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Preface

The 1948 TRANSACTIONS (volume 67) of the American Institute of Electrical Engineers will be published in two parts.

Part I contains technical program papers and related discussions presented at general meetings and District meetings during the first half of the calendar year as follows:

1. Winter general meeting, Pittsburgh, Pa., January 26–30, 1948.*
2. Great Lakes District meeting, Des Moines, Iowa, April 1–3, 1948.
3. North Eastern District meeting, New Haven, Conn., April 28–30, 1948.
4. Summer general meeting, Mexico, D. F., Mexico, June 21–25, 1948.

[Only ten Summer general meeting papers are included in Part I. The remainder of the Summer General meeting papers will be included in Part II together with the approved papers and related discussions presented at the Pacific general meeting; the Middle Eastern District meeting; the Midwest general meeting; and the Southern District meeting.]

Full correlation of all material in this volume has been accomplished by means of the multientry index beginning on page 854. Reference to any of the several subject entries for a technical paper will lead directly to the paper and to any published discussion on that paper.

Statements and opinions given in papers and discussions published in TRANSACTIONS are the expressions of contributors for which the American Institute of Electrical Engineers assumes no responsibility.

Part II of Volume 67 is expected to be available for distribution during the spring of 1949.

* (47-237 "Present-Day Grounding Practices on Power Systems"—Third AIEE Report on System Grounding, was presented at both the Winter general meeting in Pittsburgh, and the Midwest general meeting held November 3–7, 1947. This paper was included in the 1947 bound volume of TRANSACTIONS.)

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Preface

The 1948 TRANSACTIONS (volume 67) is published in two parts. The contents of Part I are listed on the preface page in Part I of the 1948 volume.

Part II contains technical program papers and related discussions presented at general and District meetings during the second half of the calendar year as follows:

1. Summer general meeting, Mexico, D. F., Mexico, June 21–25, 1948.*
2. Pacific general meeting, Spokane, Wash., August 24–27, 1948.
3. Middle Eastern District meeting, Washington, D. C., October 5–7, 1948.
4. Midwest general meeting, Milwaukee, Wis., October 18–22, 1948.
5. Southern District meeting, Birmingham, Ala., November 3–5, 1948.

* Ten Summer general meeting papers were published in Part I and the remainder are included in Part II.

Part I of volume 67 contained an index covering only the first half of the year. A complete index for 1948 (Parts I and II) is included in this volume beginning on page 1799. The Report of the Board of Directors, and the list of Officers and Committees for 1948–1949 are also included in Part II.

Statements and opinions given in papers and discussions published in TRANSACTIONS are the expressions of contributors for which the American Institute of Electrical Engineers assumes no responsibility.

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Generator Stability at Low Excitation

E. L. MICHELSON
MEMBER AIEE

L. F. LISCHER
ASSOCIATE AIEE

Synopsis: The addition of static capacitors for power factor correction on the 4-kv distribution circuits of the Chicago, Ill., system has resulted in the operation of generators at power factors close to unity during light load periods. This introduces a problem of generator stability at low levels of excitation. It is the purpose of this paper to present the results of a study on the behavior of the generators on the Chicago system during these light load periods, and to show to what extent capacitors may be added to the system without resorting to capacitor switching. The paper shows that loss of generator field presents the most serious problem in the operation of generators during such light load periods. The results of this analytical study indicate that the generators on the Chicago system may be operated safely at high power factors.

IN THE CHICAGO interconnected system the problem of generator stability within the city has been relatively unimportant in the past due to the heavy interconnections between generators in the city. The schematic diagram of the Chicago system, Figure 1, shows the extent of this interconnection between generators in the city. All 66-kv lines consist of relatively short single-conductor cables rated at 60 or 100 megavolt-amperes, and in several instances two lines are operated in parallel. The 66-to-12-kv transformers are 60 or 100 megavolt-amperes, usually two being operated in parallel. The use of phase isolated construction on the 12- and 66-kv busses throughout the city has practically eliminated the possibility of phase-to-phase faults on the major busses on the system, and phase to ground fault currents are limited by neutral impedance. As a result of the heavy interconnections and infrequency of major faults, the question of transient stability between machines in the city has not presented a serious problem.

During the past few years, static capacitors have been installed on the 4-kv distribution circuits for power factor correction. Many studies have been made which show the economic desirability of using capacitors to provide reactive power for the load instead of supplying this reactive power by other means.[1] At the end of 1946 there were approximately 100 megavolt-amperes of static capacitors connected on the 4-kv system in Chicago, and it is expected that within the next few years an additional 100 megavolt-amperes will be connected. The effect of these capacitors, coupled with the capacitance effect of the very large amount of 132 kv, 66 kv, and lower voltage cables, has resulted in the reactive load on the generators being reduced materially. Recently generator power factor has averaged as high as 0.98 lagging under light load conditions. As a result the generators have been operating at reduced levels of excitation, thus introducing the problem of generator stability at low excitation.

The problem is aggravated by the difficulty in maintaining equitable reactive distribution among the generators. To maintain satisfactory service to customers, it is necessary to control the bus voltage within rather narrow limits. This is accomplished by control of generator excitation and by adjusting taps on the tap-changers of the 66-to-12-kv transformers. Because these taps are in steps of 2.5 per cent and the impedance between generating stations is low, each tap change results in an appreciable shift of reactive power. This creates for the load dispatcher a rather difficult and lengthy task of distributing the available reactive load among the generators at a time when he is already occupied with various switching operations for maintenance purposes. Operating practice on the Chicago system has, in the past, been based on operating generators only at lagging power factors and preferably at power factors no higher than 0.95 to 0.98.

Various corrective measures have been considered to maintain lagging power factor on the generators with the increased number of capacitors on the system.[2] One solution to the problem is to disconnect the capacitors under light load conditions by clock control or current control. However, in the case of the Chicago system, the 4-kv circuits are relatively short and the switching of capacitors is not required to meet voltage standards as is the case on systems with long circuits; therefore, the major cost of capacitor switching (installation and maintenance) must be charged to the regulation of generator power factor. Also, this type of equipment on the overhead system is inherently undesirable in the urban area due to space limitations, general appearance, and difficulty of maintaining such equipment on an overhead system. The capacitors with switches can be installed in substations, but this reduces materially the possible savings. Another method for regulating generator power factor is to switch the high-voltage cables as required. However, this is an undesirable practice due to the weakening of the transmission system, the hazards involved in switching high-voltage cables, and the dispatching time required to perform the operations. Another solution which was considered was the installation of shunt reactors at generating stations, but this proved to be uneconomical. One method of regulation which has been practiced is to run synchronous condensers underexcited so that instead of gener-

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E. L. MICHELSON and L. F. LISCHER are both with the system planning division, engineering department, Commonwealth Edison Company, Chicago, Ill.

The authors wish to acknowledge the help of S. B. Crary (F '45) and C. Concordia (M '37) of the General Electric Company, Schenectady, N. Y. in connection with the methods of analysis used in this study, and D. D. Higgins (M '27) and E. Wild (M '36) of the Commonwealth Edison Company, Chicago, Ill., in connection with the various operating problems covered by the paper.

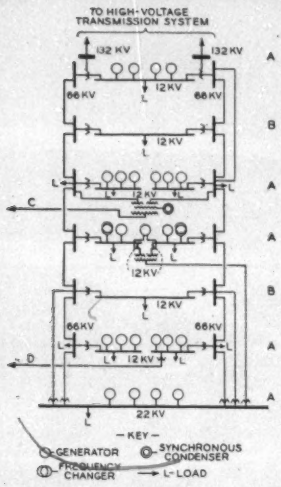

Figure 1. Schematic diagram of Chicago generating and transmission system

A—Generating station
B—Distributing station
C—220-kv high voltage transmission system
D—132-kv high voltage transmission system

ating reactive power they will take a maximum amount of reactive kilovolt-amperes from the system.

It can be seen that it is desirable to install the capacitors without switching to the greatest extent consistent with safe operation of the generating system. It is the purpose of this paper to present the results of a study which has been made for the Chicago system to determine the behavior of the generators during light load periods and to establish thereby to what extent the capacitors could be installed without automatic switching. It should be appreciated that many of the factors entering into such a study are not known with a high degree of accuracy, but it is believed that the results of the study described in this paper are sufficiently accurate to draw practical conclusions. The a-c network analyzer was used in some parts of the study, and information from actual cases of system trouble have aided greatly in analyzing the general problem and arriving at practical conclusions.

Calculation of Pull-Out Limits

As the first step in the study, calculations were made to determine the pull-out limits of a typical generator in Chicago. The problem first was considered as one of steady-state stability—to determine the pull-out limits of a generator with constant field current.[3] It was assumed that the generator is feeding into an infinite system, the rotating capacity on the solidly connected portion of the system alone at this time being approximately 800 megawatts. The machine, which was first considered, is the latest to be installed in Chicago; it has a rating of 147 megawatts at 0.85 power factor and has a short circuit ratio of 0.88. This ratio is typical of most of the machines in the city, although there are a few cases of rather extreme departure from this normal value ranging from 0.6 to 1.4. A preliminary study was made as to whether a simplified method of taking saturation into account would be adequate for the problem. This comparison is outlined in Appendix I, and, as indicated there, it was found that the use of $1/SCR$ (short-circuit ratio) as the machine impedance gives reliable results in the range of values with which this problem was concerned.

The results of the pull-out calculations are shown in Figure 2. A study of this figure indicates that there is considerable margin under practically all power factor conditions. The amount of load, as will be shown later, that any one generator will be forced to pick up under various conditions is relatively small, and the

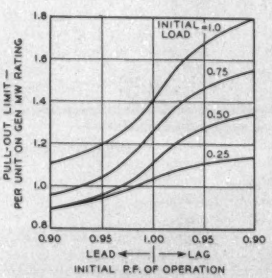

Figure 2. Pull-out limits of 147-megawatt unit in terms of initial loading and power factor with $e_t = 1.0$, $x_a = 0$, and excitation remaining constant. Curves are based on generator x_d taking saturation into account by the method shown in Figure 10(c)

Generator data:
173 megavolt-amperes, 0.85 power factor, cylindrical rotor
147 megawatts = 1.0 load
12.6 kv = 1.0 voltage
x_d (unsaturated) = 1.24
$x_l = x_d'' - .03 = 0.08$
$x_{RZ} = 0.04$
Short circuit ratio = 0.88

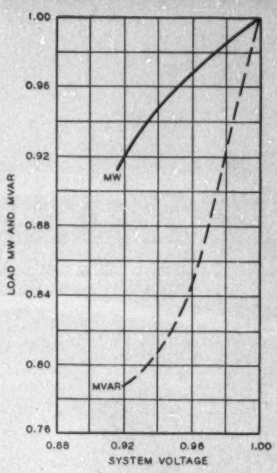

Figure 3. Load-voltage characteristic curves for total Chicago load

number of degrees that any one machine can swing ahead of the remainder is limited because of the solid interconnections between machines. These factors tend to reduce the amount of margin required for stable operation, and it is estimated that a margin of 15 per cent would be sufficient for safe operation. These curves indicate operation at leading power factors to be feasible although they demonstrate that the rate of reduction of pull-out limits in the range of power factor from 0.95 lagging to 0.95 leading is material.

Effect of Fault Conditions

The curves of Figure 2 were calculated on the basis of steady-state system conditions and terminal voltage being maintained at the normal value. However, the criterion for safe operation must be based on maintaining stable operation under various fault conditions. These fault conditions include system short circuits, sudden loss of generating capacity, and sudden opening of transmission lines; loss of field is considered later on and is treated as a special problem. A review of the transient problem involved in analysis of the fault condition indicated that the effect of the initial power factor at which the machine was operating would have little effect on the transient condition. In other words, whether a machine was operating at 0.95 lag or 0.95 lead, the ac-

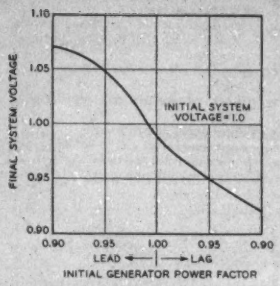

Figure 4. Final system voltage after loss of 120-megawatt generation during light load period as a function of initial generator power factor

tion under a transient condition would be approximately the same. The air-gap flux is not greatly affected by the power factor of operation within the limits under consideration. Also, with the faults being cleared within a reasonable length of time, 0.5 second or less, there is not very much decay of flux within the machine. It appeared, therefore, that analysis of these fault conditions could be made on the basis of considering the steady-state condition of the system after the fault was cleared and the load has been redistributed on the system.

A review of system conditions indicated that the worst case of loss of generation to be analyzed, was the loss of a 150-megawatt unit with the load being picked up by the remaining machines in Chicago and the various stations outside feeding in through high-voltage lines. From a consideration of system capacity and from experience during various actual cases of system trouble, it was assumed that the loss of capacity within the city would result in about 25 per cent of the power being picked up by machines within the city. It was also assumed that the reactive power deficit caused by the loss of the generator actually more important than the megawatt loss from the voltage viewpoint would be made up entirely by the machines within the city.

An important factor in determining the characteristics of the system for sudden loss of generating capacity is the variation of load with changes in system voltage. Various system tests have been performed in Chicago which have indicated the relationship between voltage and load for the combined load in the city. The results of these tests are shown in Figure 3. With the assumption and data outlined, it was possible to determine the change in system voltage resulting from the loss of a large generator, Appendix II. The results of this study are shown in Figure 4. From this curve it can be seen that even the loss of the largest generator in the city does not cause a large change in system voltage and therefore the pull-out limits of Figure 2 would not be changed appreciably. Since the pull-out limit is directly proportional to the terminal voltage, Appendix I, the limit for any value of terminal voltage other than 1.0 may still be obtained from the curves of Figure 2. An interesting point which is shown by Figure 4 is that the reduction in system voltage is less for higher power factors of operation, and if the machines are operating at an appreciable leading power factor, there will actually be an increase in system voltage when a machine is lost This tends to improve the stability of the system and offsets to some degree the reduction in stability limits resulting from low excitation.

In arriving at a conclusion for the margin required in the operation of generators in terms of the curves of Figure 2, it is also necessary to consider the governor characteristics of the individual machines and the amount the entire system frequency is likely to drop under abnormal system conditions. Governors on the Chicago system have average characteristic slopes varying between 4 and 25 per cent. In the case of the 4 per cent governor this means that for a decrease in speed of 4 per cent the machine will pick up 100 per cent load. For the case of 120-megawatt loss of generation described previously, the system frequency will drop approximately 0.1 cycle, 0.17 per cent, as shown by actual cases of trouble. It can then be seen that a 4 per cent governor with a drop in frequency of 0.5 per cent will attempt to force the machine to pick up 12.5 per cent load, 12.5 per cent of its megawatt rating. Thus a margin of 20

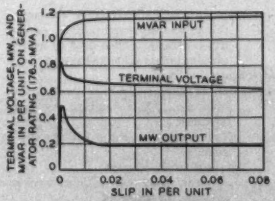

Figure 5. Terminal conditions for loss of field of 176.5-megavolt-ampere unit during light load period

All values are steady-state, average over one slip cycle

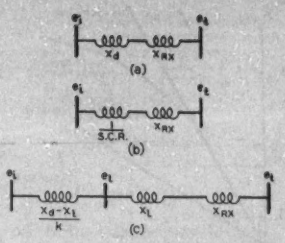

Figure 6. Equivalent circuits for generator

(a). Equivalent circuit using $x_g = x_d + x_{Rx}$ for series 1 calculations

(b). Equivalent circuit using $x_g = \frac{1}{SCR} + x_{Rx}$ for series 2 calculations

(c). Equivalent circuit using $x_g = \frac{x_d - x_1}{k} + x_l + x_{Rx}$ for series 3 calculations

per cent for a machine with a 4 per cent governor would appear to be ample, and for the great majority of turbines with less sensitive governors, 12 per cent, a 15 per cent margin is adequate.

Loss of Field

Another type of trouble to which power systems are subjected is the partial or complete loss of field on generators. In Chicago, over a period of years, there have been several such cases of trouble, although none have resulted in voltage instability of the system.[4] Such cases of trouble are caused by the incorrect opening of the field circuit breaker, connectors between field poles opening, collector ring leads burning off, exciter field resistors opening, and so forth. In order to determine the effect of change in excitation on the degree of trouble caused by loss of field, a study was made to determine the effect of loss of field of the largest machine in Chicago under both heavy and light load system conditions. For the purpose of the study, the principal effect to be determined was the reduction in the steady-state voltage of the system and no transient study was made. The method of obtaining these values is discussed in Appendix III, and the results of the calculations for the light-load system conditions are shown in Figure 5. This figure shows the relationship between the slip frequency at which the machine operates and the power output, reactive input, and system voltage. The slip frequency at which the machine will actually operate depends on the initial megawatt loading of the machine and its governor charac-

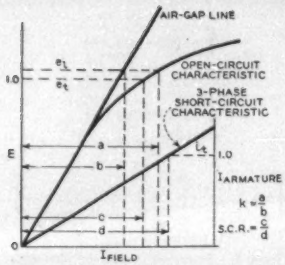

Figure 7. Determination of saturation factor k and short-circuit ratio from the generator no-load saturation and short-circuit characteristic curves

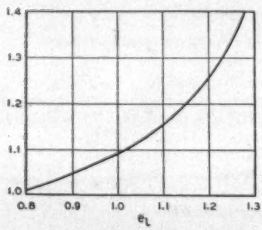

Figure 8. Saturation factor k as a function of e_l based on no-load saturation curve

teristic. However, if the machine is operating at an appreciable load, above 0.50 for the conditions indicated, the terminal conditions on the machine will fall on the flat portion of the curves, and the result will be largely independent of the exact slip frequency. From this figure it can be seen that the machine will take well over full-load current as the reactive input to the machine or approximately 200 reactive megavolt-amperes for the machine involved. With this much reactive power taken by the machine, the system voltage is reduced to approximately 0.60 for the light load system condition. The comparable figures for the heavy load system condition are an input of approximately 300 reactive megavolt-amperes and a system voltage of about 0.80.

From the standpoint of system stability, the important effect of loss of field is the reduction in system voltage, which in turn will reduce the pull-out limits of the remaining machines on the system. Table I shows the reduced value of pull-out limits for the typical remaining machines with the system voltage reduced to 0.60. This table indicates that machines carrying loads of 0.75 of their rating or above, will be operating at or near their pull-out limits. On the other hand, these machines will be near their stability limit even with the lagging power factors which have in the past been considered safe operating points. It therefore appears that the magnitude of the problem created by loss of field is not greatly affected by the number of static capacitors which may be added to the system.

Conclusions

This paper describes the results of a study made for the Chicago system to determine the stability of the generators during light load system conditions, and to establish therefrom to what extent unswitched static capacitors could be added to the distribution system. The conclusions which have been reached from this analytical study of the Chicago system are as follows

1. An analysis of the pull-out curves for a typical generator as shown in Figure 2 shows that with constant-terminal voltage and constant-field current, generators can operate in the leading power-factor zone and still have ample margin to pick up an increment of load greater than that which the machines will be required to pick up under abnormal operating conditions.

2. Within the limited range of generator power factor considered in this paper, the initial power factor of the machines has very little effect on the transient stability of the system.

3. The loss of power supply due to the loss of the largest unit will not create a sufficient reduction in voltage and change in operating conditions to cause instability, even with power factors in the leading zone.

From these facts it may be concluded that unswitched capacitors may be added to the 4-kv distribution system until a generator power factor of 1.0 is reached.

Nomenclature and Symbols

e_t = Generator terminal voltage (assuming the generator reactor to be part of the generator); also used as system voltage unless otherwise specified
e_e = System voltage
e_i = Internal generator voltage—voltage behind x_d, $1/SCR$, and so forth, as specified
e_l = Voltage behind generator leakage reactance, x_l
i_t = Total armature current
x_d = Generator direct axis synchronous reactance, unsaturated
x_d' = Generator direct axis transient reactance
x_d'' = Generator direct axis subtransient reactance
x_l = Armature leakage reactance
x_{RR} = Generator reactor reactance
x_c = External reactance
x_g = Generator reactance, including reactor, based on x_d, $1/SCR$, and so forth, as specified
x_{rd} = Rotor direct axis synchronous reactance
x_{rq} = Rotor quadrature axis synchronous reactance
r_{rd} = Rotor direct axis resistance
r_{rq} = Rotor quadrature axis resistance
x_r = Equivalent rotor reactance
r_r = Equivalent rotor resistance

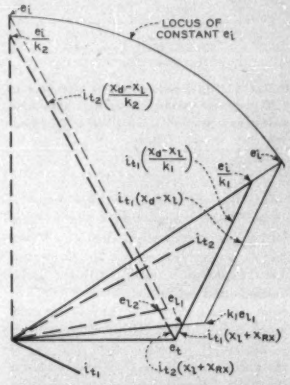

Figure 9. Vector representation for determining pull-out point of generator with initial load of 1.0 at 0.90 power factor lag taking saturation into account by use of saturation factor k

e_t and e_l are assumed constant
Subscripts 1—Vectors at initial operating point
Subscripts 2—Vectors at pull-out point

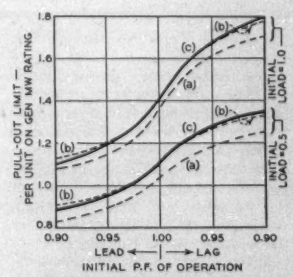

Figure 10. Comparison of pull-out limits of 147-megawatt unit for initial loading of 1.0 and 0.5, with e_t=1.0, x_c=0, and constant excitation

(a). Based on $x_g = x_d$ (unsaturated)

(b). Based on $x_g = \frac{1}{SCR}$

(c). Based on $x_g = x_l + \frac{x_d - x_l}{k}$

k = Saturation factor as obtained from generator no-load saturation curve
SCR = generator short-circuit ratio

Appendix I

Determination of Generator Pull-Out Curves

It is well known that the effect of saturation on the pull-out characteristics of a generator is appreciable.[5-8] Because of the

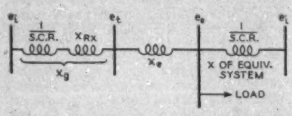

Figure 11. Equivalent circuit for the case of generator connected to a noninfinite system

large number of generators in Chicago and a correspondingly wide range of generator constants (such as x_d, short circuit ratio, and so forth), three series of calculations were made for one generator with fairly typical constants to determine how the saturation effect may most easily be taken into account with reasonable accuracy. The calculations of series 1 neglect saturation; those of series 2 assume a constant value of saturation; and those of series 3 assume a variable value of saturation. The following assumptions are common to all three series of calculations:

1. Terminal voltage remains constant; $e_t = 1.0$
2. Field excitation remains constant at initial operating point (no automatic voltage regulation).
3. Generator losses (resistance) were neglected so that the equation for maximum generator power output becomes

$$P_{max} = \frac{e_l e_t}{x_g}$$

4. Generator data
Rating: 173 megavolt amperes at 0.85 power factor (2-winding cylindrical rotor)
147 megawatts = 1.0 load
12.6 kv = 1.0 voltage
x_d (unsaturated) = 1.24, per unit on 173 megavolt amperes
x_{Rx} = 0.04, per unit on 173 megavolt amperes
$x_l = x_d'' - .03 = 0.08$, per unit on 173 megavolt amperes
Short circuit ratio = 0.88 at 12.6 kv

$$\frac{1}{\text{Short circuit ratio}} = 1.17 \text{ (including reactor), per unit on 173 megavolt amperes}$$

Series 1. Based on $x_g = x_d$ (unsaturated). See Figure 6*a* for the equivalent circuit used. Neglecting saturation will, of course, give pessimistic results and these were used as a guide. See Figure 10, curve *a*.

Series 2. Based on $x_g = \frac{1}{SCR}$. This takes saturation into account but assumes it to be constant for all ranges of generator loading and power factor. See Figure 6*b* for the equivalent circuit used. Values of pull-out limits obtained this way, in the range of 0.9 power factor lag to 0.9 power factor lead, are from 3 to 11 per cent higher, depending on power factor and loading, than those in Series 1. See Figure 10, curve *b*.

Series 3. Based on

$$x_g = x_l + \frac{x_d - x_l}{k}$$

maximum power then becomes:

$$P_{max} = \frac{(e_l/k)(e_t)}{x_l + \frac{x_d - x_l}{k}}$$

This takes saturation into account fairly accurately (as it varies with generator loading and power factor right up to the pull-out point). The saturation factor k was obtained from the no-load saturation curve as is shown in Figure 7. It should, strictly

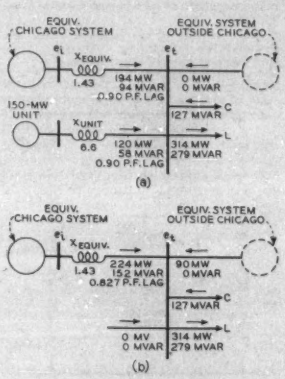

Figure 12. System equivalents for loss of unit during light load period

C = equivalent net high-voltage cable charging capacity and 4-kv capacitors
L = equivalent system load inside Chicago
$x_{equiv.}$ = equivalent system reactance using

$$x_e = \frac{1}{SCR} + x_{Rx}$$

expressed in per unit on 920-megavolt-ampere base
(a). Condition before loss of unit
(b). Condition after loss of unit but before adjusting megawatts and reactive megavolt-amperes for new e_t

speaking, be obtained from the load-saturation curves, but normally these are not available. Values of k may then be plotted against varying values of e_l. Figure 8 shows how k varies with e_l. The calculations for pull-out limits can be made by using the graphic method or the a-c network analyzer, but either way it is a trial and error method. Figure 6*c* shows the equivalent circuit used and Figure 9 shows the vector diagram for one condition of initial loading. The pull-out limits as obtained this way, Figure 10*c*, agree very closely with those in Series 2. At power factor = 1.0 the values are identical; at power factor = 0.9 lag the values are 1 per cent higher; at power factor = 0.9 lead the values are 2 per cent lower—this is to be expected because there is less saturation at leading power factor.
Rewriting

$$P_{max} = \frac{(e_l/k)(e_t)}{x_l + \frac{x_d - x_l}{k}}$$

as

$$P_{max} = \frac{e_l e_t}{x_d - x_l + k x_l}$$

the effect of k becomes more readily discernible. For example: for initial load = 1.0 at 0.9 power factor lag, k will be 1.12; at the pull-out point i_l will lead e_t by approximately 30 degrees and k then will be 1.05. The change in the value of k, after determining the initial e_l, has practically no effect on P_{max}, since x_l is approximately equal to $0.1\,(x_d - x_l)$.

Results. In Figure 10 are plotted the pull-out values of the generator for 1.0 and 0.5 initial load as obtained by the three series of calculations. It can be seen that, for the accuracy required in most practical applications, using $x_g = 1/SCR$ is adequate in the range of generator power factor = 0.9 lag to 0.9 lead.

Effect of external reactance. If it may not be assumed that the generator under consideration feeds directly into an infinite bus, then the effect of external reactance must be taken into account. The x_e may be handled in similar manner as the x_{Rx}. Carrying through one series of calculations when $x_e = 0.08$, then P_{max} as shown in Figure 2 is reduced by approximately 6 per cent. If the amount of rotating capacity on the system is not great enough to permit the assumption of an infinite system, then the equivalent circuit as shown in Figure 11 may be used as an alternative.

Table I. Reduction in Pull-Out Limits Caused by Reduced Terminal Voltage

Initial Gen. P.F.	Initial Gen. Load	Pull-Out Limit $e_t = 1.0$	Pull-Out Limit $e_t = 0.60$
0.98 lag	1.0	1.55	0.93
	0.75	1.37	0.82
	0.50	1.20	0.72
	0.25	1.08	0.65
1.0	1.0	1.41	0.85
	0.75	1.26	0.75
	0.50	1.12	0.67
	0.25	1.04	0.62
0.98 lead	1.0	1.30	0.77
	0.75	1.15	0.69
	0.50	1.03	0.62
	0.25	1.00	0.60

Appendix II

Determination of System Voltage After Loss of a Large Unit During Light Load Period

In determining the system voltage after loss of a large unit, the first assumption that

must be made is the amount of load the remaining units must pick up. In the case under consideration the remaining generators in the city would pick up 20 to 25 per cent of the megawatt generation lost, as shown by actual cases of trouble; the remainder of the load would then be picked up by generators outside the city feeding in through high-voltage lines. It was further assumed that the generators outside of the city would not pick up any of the reactive generation lost because these generators would not feel appreciably the drop in voltage.

It is of considerable help if the load-voltage characteristic of the system load is known.[8] Figure 3 shows the load-voltage characteristic of the Chicago system load as obtained from actual system tests. In the calculation for determining system voltage after loss of a unit, the drop in load due to the drop in voltage was taken into account. Because the load-voltage curves are about four years old, they actually do not include the effect of 4-kv capacitors; hence the capacitors have been lumped in with the cable charging effect, and this too was corrected for change in voltage. No automatic voltage regulation was assumed.

Figure 12 shows the equivalent circuits used in this analysis with figures for the condition of generator power factor equals 0.90 lag.

Appendix III

Determination of Terminal Conditions for Loss of Generator Field

To simplify and reduce the problem to its basic elements, the following assumptions were made:

1. The problem was solved for the steady-state condition, assuming that the transient condition would cause no undue distress to the system. The rate of decrease of system voltage will be comparatively slow and the worst system condition will exist when the machine has reached its ultimate value of slip.[9] The amount of this slip will depend on the amount of initial load on the generator, and the turbine governor characteristic.

2. The loss of field was taken to be a complete loss, such as that obtained by opening the field switch, so that the generator field discharge resistor is connected across the generator field.

3. Rotor losses were neglected. These were determined to be approximately 0.04 at 0.045 slip, most of this loss being in the iron. Saturation was neglected.

4. All of the remaining generators on the system were combined into one equivalent generator. The equivalent reactance was obtained from values of short-circuit megavolt-amperes using generator synchronous reactance.

5. The load was permitted to drop as the voltage decreased. Because the decrease in voltage was so great on loss of field and the load-voltage characteristic curves do not go below 0.92 voltage, the best compromise was thought to be that of treating the load as a constant impedance load.[11] This is somewhat optimistic, as a check shows that by assuming constant impedance, the terminal voltage in one instance is 0.80, whereas an adjusted value of terminal voltage, based on extrapolation of load-voltage curves, shows that a_t might more nearly be between 0.75 and 0.77. However, because so little is known of composite load-voltage characteristics at low voltages, 0.60 to 0.90, it was felt that using constant impedance load and viewing the results as being optimistic by perhaps 5 per cent was a reasonable solution.

6. Characteristics of the generator which was assumed to lose its field:

176.5 megavolt-amperes, cylindrical rotor
150 megawatts, 0.85 power factor

$x_d = 1.27$, on 176.5 megavolt amperes
$x_l = 0.10$, on 176.5 megavolt amperes
$x_{Rx} = 0.09$, on 176.5 megavolt amperes
$SCR = 0.85$
Speed = 1,800 rpm

7. No automatic voltage regulation was assumed.

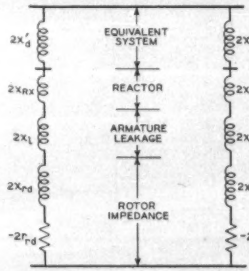

Figure 14. Equivalent circuit for combining direct and quadrature axis impedance into equivalent x_r and $-r_r$

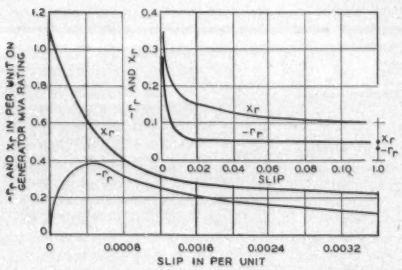

Figure 13. Variation of equivalent $-r_r$ and x_r with slip; these are average values over one slip cycle

Curves are for typical large solid-rotor turbogenerator

The variation of rotor x_r and $-r_r$ with slip of the unit under consideration must be known. Figure 13 shows such curves for a typical large solid-rotor turbogenerator. This information is usually obtained in terms of rotor x_{rd}, x_{rq}, $-r_{rd}$, and $-r_{rq}$; the in-phase and quadrature components may be combined into equivalent x_r and $-r_r$ by using an equivalent circuit such as is shown in Figure 14. Note that equivalent system transient reactance is used in this combination. It is also to be noted that these values of x_r and $-r_r$ are the average values over one slip cycle assuming constant slip.

The solution then for the terminal voltage, power output, and reactive input of the generator which has lost its field is most readily accomplished by using the a-c network analyzer; the generator may be represented by two reactances and a power-source in series, the power source being used to

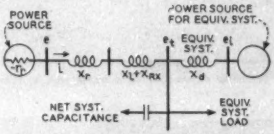

Figure 15. Equivalent circuit for determining terminal conditions for loss of generator field

obtain $-r$ by inserting a series in-phase voltage of such magnitude that $e/i = -r$. The remainder of the system is set up by equivalents as is shown in Figure 15.

References

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Discussion

T. W. Schroeder (The United Light and Railways Service Company, Davenport, Iowa): The question of generator stability when operating at high power factors during light system load is one which is brought up frequently in connection with the application of capacitors to power systems. The authors not only show that for the Chicago system the operation of generators at high lagging or even leading power factors creates no light load stability problem, a conclusion previously reasoned to be generally true,[1] but they also indicate high power factor operation practically does not aggravate the problem of loss of a machine field.

A very important factor in the light load stability problem, ably pointed out in the paper by Michelson and Lischer, is that in case of a sudden system load increase at light load, all machines on the system initially share the load increase as determined by their governor characteristics, so that no single machine is required to suddenly carry a large block of load, and a moderate stability margin is all that is needed. The only possibility of a problem is created if an operator, in loading a previously lightly loaded machine in redistributing load following a load increase, should fail to increase excitation of that machine at the same time. Even this problem is obviated if the machine excitation is under regulator rather than hand control.

In the calculations made by the authors to produce the data shown in Figure 2 of the paper, it is probably quite satisfactory to assume as they did that the machine operates against an infinite bus (use generator circuit impedance only and fixed terminal voltage regardless of load.)

To obtain an idea of the stability limits for various initial loads and power factors with a machine operating against a system of limited capacity, Figure 1 of this discussion was prepared using the method outlined in Appendix I of the paper. The conditions assumed were those which it is expected may obtain at light load on the United Light interconnected system after its several systems are in interconnected operation.[2] These conditions are

All units on one of the systems down except one 35-megawatt 0.8-power-factor. Balance of load carried by remote systems over 161-kv transmission line.

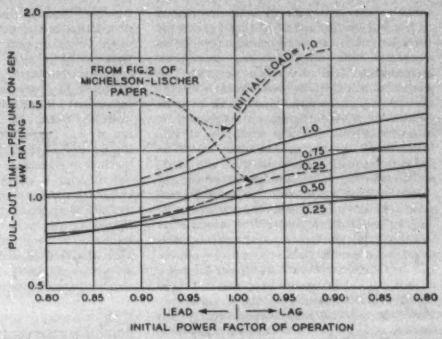
Other data:

Generator rating 43,750 kva, 0.8 power factor, 3,600 rpm
35,000 kw = 1.0 load
Short-circuit ratio = 0.9
X_d (external system reactance) = 29.3 per cent on 43,750-kva base

The results shown on Figure 1 of this discussion are probably not as accurate as those of Figure 2 of the paper, because the introduction of external system reactance into the calculation method results in a machine terminal voltage which varies with load on the generator being considered; the calculated terminal voltage at pull-out for the various power limits indicated on Figure 1 being as low as approximately 80 per cent of normal. If there is a good share of load on the same bus with the generator, this would lead to the conclusion that such load would be considerably reduced at pull-out; however, actually, with local load and maximum angular separation between machine and system, more of the local load will be supplied by the machine with the result that the load voltage will be held up.

Figure 1 in general indicates minimum stability margins of about 25 per cent, which in light of the previous discussion shows that high generator operating power factors are permissible even on systems of limited interplant capacity.

Figure 1. Stability limits



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C. G. Adams (General Electric Company, Schenectady, N. Y.): It is pleasing to see that a practical system problem has been thoroughly analyzed by the authors resulting in justification of generator operation at low excitation where it is economically advantageous and simplifies operating procedures. It is noteworthy that correction to unity power factor at the generating station is allowable under manual control of excitation without substantially increasing the chance of system instability during a severe system disturbance caused by loss of field on a large machine. Automatic control of excitation and the use of out-of-step and loss-of-field relaying would serve to localize and quickly eliminate the system voltage disturbance.

Since the use of voltage regulators has not been mentioned, it would be interesting to know how their application would have affected the amount of unswitched capacitors that could have reasonably been applied in the distribution area of the system. This might result in generator operation at appreciable leading power factors at light load. Further conclusions that the authors may have reached based on the use of modern excitation systems would be most interesting.

W. H. Osterle (West Penn Power Company, Pittsburgh, Pa.): The West Penn Power Company has been concerned with high power factor on its generators for the same reason that actuated the study presented in this paper, namely the addition of static capacitors. Our load is naturally at high power factor at the generator terminals, the reactive requirements being such that a power factor of near 95 per cent is obtained throughout the 168 hours in the week. In order to carry the peak load nearly all reserve steam in the turbine is converted to kilowatts with only the reactive necessary for voltage. Static capacitors are installed at attended substations to hold up the system voltage on peak and are switched off if voltage becomes excessive under light loads.

A static stability study was made somewhat similar to that presented in this paper without arriving, however, at definite conclusions. Factors other than pull-out limits seemed to confuse the issue. It is with a great deal of pleasure therefore that we welcome this paper and others on the subject, as they should assist in arriving at sound operating procedure. We would like to ask the authors their opinion of our method and what led them to disregard some of the factors we considered.

Our method differs, first, in the determination of E_f or generator internal voltage. The authors assume a constant terminal voltage and calculate E_f by using various values of X_d. We obtained the E_f from the generator no-load and short-circuit saturation curve by determining the field excitation necessary to produce a given generator loading power factor, and terminal voltage then read E_f on the air gap line. This we say is the generator internal voltage necessary to give the proper value of E_t, allowing for armature reaction and generator reactance drop.

Our second difference is in the voltage we hold constant. The authors have assumed the terminal voltage constant, whereas we have considered an infinite bus voltage held constant. The infinite bus in our case was the 132 kv-bus at Springdale (Pa.) since the Springdale generators were being studied. This bus is supplied by five of the seven generators at Springdale through their own step-up transformer banks, and also by the entire interconnection through seven 132-kv lines.

Our third difference is in the external impedance used. The authors used no external impedance. We used an equivalent X_e to our infinite bus by paralleling all contributions of fault current to a 3-phase 132-kv bus fault. Our system is set up on our own d-c board with all power sources behind

subtransient reactance. A fault was placed on the 132-kv Springdale bus and each contribution reduced to equivalent reactance on a common base. These branches were then increased by the difference between the generator transient and sub-transient reactance and all paths paralleled. This gave an equivalent system reactance to generator internals behind transient reactance viewed from the 132-kv bus. This value, added to the generator X_d in question plus the step-up transformer reactance, gave the X_{total}.

In all of our calculations we used X_d unsaturated as we felt that at the point of pull-out the terminal voltage will be low enough to cause the generator to operate on the unsaturated portion of the saturation curve. Our calculations indicate a pull-out for most of our generators at unity power factor in excess of 1.15 when operating at full load. However, our full load is more nearly the turbine rating than the authors'. Assume a turbine rating equal to the megavolt-amperes of the machine—then the authors' generator would have a turbine of 1.177 per unit yet the calculated pull-out is in excess of 1.2 from 95 per cent power factor lead in the lagging direction. Does this mean that it would be impossible to reach a state of static instability in this region?

With so many generators in an interconnected system the incremental load picked up by each for the loss of a large block of power (generator or tie line) is rather small, depending upon the governor droop. Generators with large droop will pick up very little. The authors say only 25 per cent of the loss of a 150-megawatt unit will be picked up in the Chicago system. Furthermore, West Penn Power Company and many other utilities have voltage regulators on their generators which again help during system disturbance. Do not these factors minimize the case for static instability?

The authors' loss of field discussion is certainly extremely interesting and points up a condition that requires study. However, there are a few factors on which we are not clear. Do the conclusions pertain only to those machines on the same bus as the machine that loses field? Would the loss of the field of a generator at one generating station affect the generator at another station? Does the 60 per cent voltage refer only to the generator bus at the station involved or to the system voltage?

The authors have done a splendid job in presenting this rather technical subject of static stability and should do much to dispel doubt regarding the feasibility of operating generators at low excitation.

J. B. Ward (Purdue University, Lafayette, Ind.): This paper presents an interesting and useful analysis of limitations on generator operation at low excitation and a particularly interesting treatment of the loss of field on a large unit. From the standpoint of application to other systems a certain aspect of the method of analysis may warrant further clarification. In calculating the pull-out limits of a particular machine the authors assume that the machine in question is connected to an infinite bus. Then, in studying the effects of a generator outage or of loss of field, the system is represented as a noninfinite equivalent machine, and the curves of Figure 2 in the paper, for a generator connected to an infinite bus, are modified by adjusting the pull-out limits in proportion to the terminal voltage variation of the actual system. It seems to the writer that justification of this as a general procedure is not clear.

Figure 11 of the paper shows a suggested equivalent circuit for a non-infinite system, however, details on handling this case are not given. It may be of interest to compare the method of the paper with an alternative method in which the system is treated consistently as a noninfinite equivalent machine in a circuit similar to Figure 11. The method of approach described herein is presented for the purpose of illustrating, in a somewhat different manner, the effects of a generator outage on the pull-out limits of a particular machine.

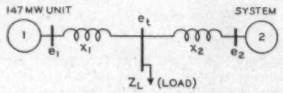

Figure 2. Equivalent circuit

$X_1 = \frac{1}{SCR} + X_R = 0.68$, per unit on 100 megavolt-amperes

$X_2 = 0.1$, per unit on 100 megavolt-amperes (assumed)

$e_t = 1.0$ per unit (initial value)

Load = 314 megawatts and 152 reactive megavolt-amperes (lagging)

$Z_L = 0.258 + j0.125$, per unit on 100 megavolt-amperes

e_1 and e_2 calculated for each initial loading

The value of equivalent system reactance of Figure 15 is not given, but data of the paper indicate that this reactance is in the order of 0.1 to 0.13 per unit on 100 megavolt-amperes. A value of 0.1 is used here. In Figure 2 of this discussion, the 147-megawatt unit is to be tested for pull-out limits at various initial loadings, the remaining generating capacity is lumped into an equivalent machine, and Z_L is composed of the combined system load and static capacitors. This load is treated as a constant impedance.

The pull-out power of the 147-megawatt unit may be obtained by the familiar equation

$$\text{Max } P_1 = \frac{e_1^2}{Z_{11}} \sin \alpha_{11} + \frac{e_1 e_2}{Z_{12}} \qquad (1)$$

where

Z_{11} is the magnitude of the driving point impedance viewed from e_1

Z_{12} is the magnitude of the transfer impedance from e_1 to e_2

$\alpha_{11} = 90$ degrees $- \theta_{11}$ (θ_{11} is the impedance angle of Z_{11}).

The driving point impedance is calculated by paralleling the load impedance with x_2 and adding the result to x_1. The transfer impedance is obtained by making a wye-delta transformation. Table I of this discussion compares the results obtained by this method with data from Figure 2 of the paper for an initial loading on the 147-megawatt unit of 0.5 per unit.

In a metropolitan system with small equivalent system reactance and large load the solution might be simplified, without introducing large errors, by making two additional approximations

1. Neglect the first term of equation 1 since under the conditions specified α_{11} will generally be very small.

2. In computing the transfer impedance, neglect the load resistance and use for Z_L a reactance corresponding to the load reactive at the specified initial terminal voltage. This simplifies the wye-delta transformation.

The assumption of constant impedance load does not seem to be too serious, at least for the range of voltage variation included in Figure 3 of the paper. Check calculations were made at a terminal voltage of 0.92 per unit by modifying the load impedance in Figure 2 of this discussion to conform with the load characteristics shown in the paper. In these calculations the capacitors were separated and not modified since they conform to the constant impedance assumption. The effect of including load characteristics is to decrease the transfer impedance and increase the pull-out power approximately 1 per cent over that listed in Table I of this discussion, referring to the values obtained by the present method.

If the 147-megawatt unit is called upon to pick up load as a result of the loss of a large unit on the system, the internal voltage e_1 would remain the same, e_2 would not be appreciably affected, and both Z_{11} and Z_{12} would increase slightly due to the increase in equivalent system reactance. In considering the problem from this viewpoint it is difficult to reconcile the statement made in the paper, referring to Figure 4, that if the machine which is lost were operating at leading power factor, the pull-out limit would increase with the resulting rise in terminal voltage.

Applying the equivalent circuit of Figure 2 of this discussion to the calculation of the pull-out limit following the loss of field on one of the large units, assume that one of the machines included in the system equivalent suffers the loss of its field, and that it is desired to determine the pull-out limits of the 147-megawatt unit at various initial loadings. If the machine without field excitation imposes a reactive load on the system of 200 reactive-megavolt-amperes and reduces the terminal voltage to 0.65 per unit as shown in Figure 5, this may be represented in Figure 2 of this discussion by a reactance connected in parallel with Z_L and adjusted to produce a load of 200 reactive-megavolt-amperes at the predicted voltage. Treating the original load Z_L as a constant impedance, this parallel combination forms a new load which in turn modifies the driving point and transfer impedances. Also the equivalent system reactance increases as a result of the separation of the unit without field excitation. Calculations show that for an initial loading of 0.5 per unit on 147-megawatts at unity power factor this method gives a pull-out limit of 62 per cent of that shown in Figure 2 in the paper.

The numerical results compare favorably for this particular system. However, in terms of equation 1, of this discussion, the pull-out power is not proportional to the terminal voltage following an outage, assuming constant excitation voltages as established before the outage. This might be of importance in applying the method of the paper to a different system.

E. L. Michelson and **L. F. Lischer:** The discussers in their comments have added much that is of value and is useful in the analysis of this problem as applied to other systems.

In regards to generator pull-out calculations, the method mentioned by Osterle of determining the generator internal voltage for a given load and power factor from the saturation curves, starting with the known value of field current, does not differ essentially from the method of using $x_g = 1/\text{short-circuit ratio}$. In either case, saturation is taken into account. In one, by using values of field current and the saturation curves; in the other, by using a modified synchronous reactance—1/short-circuit ratio. It may be of interest that values of field current as obtained by calculation using $x_g = 1/\text{short-circuit ratio}$ agree very closely with actual readings of field current for different generator loadings in the region of 0.90-lag to 0.98-lag power factor. We agree that there is very little, if any, saturation in the machine at the pull-out point (particularly if the terminal voltage has decreased). However, saturation should be considered in that it does determine the internal voltage (or field current) which exists at the initial loading—this value of internal voltage then being constant in the case of pull-out under fixed excitation.

As to whether the generator feeds directly into an infinite bus or whether external reactance must be considered, thus allowing the terminal voltage to vary, depends, of course, entirely on the system under consideration. What may be justifiable in this respect on the Chicago system may not apply at all on other systems where there is more reactance between generators. This point is brought out in Appendix 1 of the paper but perhaps not too clearly. The data presented by Ward and Schroeder is a valuable addition to this phase of the problem.

Osterle asks whether it actually might be impossible to reach the steady-state pull-out point because the limit is above the turbine rating. It appears that this condition is possible if the terminal voltage stays close to 100 per cent. In this connection, the value of automatic voltage control is very apparent and as has been pointed out in oral discussions at the presentation, automatic voltage control in sufficient quantity practically eliminates the problem.

Table I. Comparison of Pull-Out Limits at Constant Excitation by Two Methods

Initial Loading on 147-Megawatt Unit of 0.5 Per Unit of Machine Megawatt Rating

Initial Power-factor	Pull-Out Limit in Per Unit on Machine Megawatt Rating: By Method of This Discussion	Pull-Out Limit in Per Unit on Machine Megawatt Rating: From Figure 2 of the Paper
0.9 lag	1.23	1.34
1.0	1.02	1.11
0.9 lead	0.83	0.90

In regards to loss of field, it is true that the terminal voltage of 60 per cent on loss of field, as shown on the curves, is the voltage on the terminal of that machine and the other machines at this same station. The generators at other stations would be at a somewhat higher voltage, but not much on the Chicago system because of the low reactance between stations. Hence, if nothing were done to remove the generator that has lost its field from the system, some of the other generators would pull out of synchronism, resulting in still lower voltages. Loss-of-field relays on the larger machines to remove the machine on loss of field from the system automatically are the answer to this problem.

Ward's analysis of treating the equivalent system consistently as a noninfinite equivalent machine is very interesting and certainly will apply on a large number of systems. Where the assumption of an infinite system cannot be made, his or some similar method[1] should be used, such as Figure 11 of the paper, wherein the method of handling this circuit is similar to the method of Ward neglecting the first term of his equation *1*. However, his own figures tend to prove the validity of the assumption of an infinite system when the equivalent system reactance is 0.1 on 100 megavolt-amperes or less. Such an assumption, of course, permits rapid and easy solution without involving the use of an a-c network analyzer. It is recognized that the infinite system assumption must give results that are on the optimistic side by a small amount which can easily be determined from the order of magnitude of the equivalent system reactance.

Adjusting the pull-out limits in proportion to the actual terminal voltage at the time of pull-out is, of course, permissible only on the infinite system assumption and was not intended as a general procedure. If the equivalent system is assumed zero reactance, then the pull-out power must vary directly with the terminal voltage. Note the close agreement obtained for pull-out of a generator when another machine has lost its field—by Mr. Ward's method the generator pull out is 0.62 of that shown in Figure 2 of the paper; by the authors' method, the generator pull-out is 0.65 of that shown in Figure 2, the generator terminal voltage being 0.65 at this time. The same argument holds true for a rise in system voltage on loss of a machine that has been operating at a leading power factor. It would appear that the pull-out margin required as pointed out in the paper must of necessity be large enough, taking all other assumptions into consideration, so that a minor discrepancy in calculated pull-out limits is of no serious consequence.

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Location of Gas Leaks in Pipe-Encased Gas Pressure Cable Lines

JOHN D. PIPER
NONMEMBER AIEE

WHEN The Detroit Edison Company decided several years ago to install a gas pressure cable within a steel pipe for an approximately 7-mile 120,000-volt transmission line between Delray Power House and Warren Station, questions arose concerning means for locating gas leaks, should any develop. No method that could be expected to give reasonably reliable results was available, and accordingly a method had to be devised. The purpose of this paper is to describe that method.

The method is applicable for use with pipe-contained cables that operate under gas pressure, whether the gas is in contact with the insulation, as in gas pressure cable, or is separated from the insulation by a sheath, as in compression cables. In addition, it seems probable that, by introducing minor modifications, the method may be applied to self-contained gas pressure cables as well. The method has given good results both in locating a simulated leak at a joint in the pipe line, and in locating actual leaks in the terminals of the line mentioned.

To avoid the implication that pipe-contained gas pressure cables are inherently subject to leaking, it seems advisable to state that, except at terminals, no detectable leaks have thus far developed in The Detroit Edison Company's gas-pressure line.

Structural Features of Line

Structural features of the cable line pertinent to the development of the leak test are shown in Figure 1. Three single-conductor cables are contained in a steel pipe as shown in diagram A. Approximately 40-foot lengths of the pipe are welded together to form sections ranging from slightly less than 400 to nearly 1,500 feet in length. These sections, which contain continuous lengths of cables, are connected by joints in which the cables are spliced. Twenty-three of these joints are termed normal joints, diagram B, which provide a continuous gas passage. Near the end of each joint is a gas sampling valve. The other six joints are stop joints, diagram C. Each provides a gas barrier. The barrier is shunted by a by-pass in which a by-pass valve, normally kept open, is located. A test valve is located on each side of the by-pass valve. The stop joints, two of which are at the ends of the pipe line, separate the line into five stop-joint-to-stop-joint sections, as in diagram D. In addition, there are two terminal sections as shown. In these, the three cables run from the stop joint to their respective potheads in three separate pipes called sweeps. A test valve is located at the base of each pothead.

The line operates under a nitrogen pressure of approximately 200 pounds per square inch. The volumes of the gas space surrounding the cable in the sweeps, the pipe line, and the joints are known with fair accuracy, and the average temperature of the gas is obtainable from measurements made at several thermocouple stations.

Requirements of Test Method

The first requirement of the test method was that the use of it would not adversely affect the cable. A second was that service of the line should be interrupted as little as possible. Preferably, service should be interrupted only if it became unfeasible to keep the line at operating pressure. The third requirement was that the method be sufficiently sensitive to detect and locate the minimum size of leak that would be sought. Originally this value was set at one tankful, or 200 cubic feet at atmospheric pressure, of nitrogen per day. Later, when it became apparent that the method was sufficiently sensitive to reveal leaks of much smaller magnitudes, it was decided that the presence of these smaller leaks should be detected as they occurred but should not necessarily be located.

Practically all of the methods used for locating leaks in manufactured or natural gas lines were eliminated by the requirements, especially that of the minimum rate, which is much lower than that tolerated by most gas companies for the same length of line. Most odorants, including those used in manufactured or natural gases, were considered objectionable because they might be absorbed by, and deleteriously affect, the insulation of the cable. In addition, the odorants would be absorbed by the soil, thus diminishing their effectiveness, and, especially in paved areas, might travel in underground passageways a considerable distance from the leak before emerging from the ground. Because the gas used in the cable line was nitrogen, methods based upon the effect of the escaping gas on vegetation were of no avail and detection of nitrogen as it escapes into the air was considered impractical.

Test methods involving radioactive tracer gases were also considered but were rejected because, among several other reasons, they might promote ionization in the cable and be dangerous to personnel. Use of combustible and toxic tracer gases was also rejected because of possible danger to personnel or equipment.

Methods based upon the resistance of

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JOHN D. PIPER is with the research department of The Detroit Edison Company, Detroit, Mich.

The author is grateful to the following colleagues, past and present, for their contributions to the principles, instruments, and data described herein, E. B. Baker, Eugene Brown, F. M. Hull (A'33), M. R. Lewis, R. J. McCausey, and Franklin Schlachter.

flow of gas through the line, such as are used for locating gas leaks in telephone cables, could not be used because the resistance to flow through the 7-inch pipe line is negligible as compared with the resistance of flow through the test valves. These valves furnish the only practical means by which instruments for determining conditions within the gas space could be attached. Stethescopic and other listening devices proved to be unsuccessful because of other noises.

Principle of Method Adopted

The method finally adopted consists of three steps. Step 1 shows which, if any, stop-joint-to-stop-joint or terminal sections contain leaks and the rate of each. Step 2 locates a leak within a stop-joint-to-stop-joint section to the affected joint-to-joint section. Step 3 locates a leak within a joint-to-joint or terminal section with respect to a selected point, such as the position of a test valve.

Step 1 depends upon the principle that, if gas escapes from a space of constant temperature and volume, the pressure drops. If the temperature remains constant the rate of the gas leak is proportional to the product of the volume of the gas space and the rate at which the pressure drops.

The principle of Step 2 is illustrated in Figure 2. A tracer gas is introduced, as shown by ↓ diagram *A*, at the gas sampling valve of a normal joint near the midpoint of the affected stop-joint-to-stop-joint section. The rate at which the tracer gas is introduced is adjusted to compensate exactly for the rate of the leak. The tracer gas migrates toward the leak resulting in a column that extends a short distance from the point of introduction, as shown. After a chosen quantity of the tracer gas has been introduced, the source of supply is removed and is replaced by a gas analyzer. A quantity of nitrogen equal to half that of the tracer gas is then introduced rapidly at the joint next to the analyzer in one direction and is withdrawn simultaneously at the joint next to the analyzer in the opposite direction. If the leak happens to be in the direction from which the nitrogen was introduced, the column of tracer gas is brought beneath the sampling valve, as in diagram *B*, with the result that the analyzer indicates the presence of the tracer gas. If, on the other hand, the leak happens to be in the opposite direction from which the nitrogen was introduced the column is pushed away from the sampling valve, as in diagram *C*, and the analyzer shows the gas to be nitrogen. In this case the direction toward the leak is confirmed, diagram *D*, by pushing the column in the reverse direction until it is beneath the sampling valve. For effecting the latter shift, the points of introduction and withdrawal of the nitrogen are reversed and the quantity used is equal to the quantity of tracer gas introduced.

Real gases, of course, mix in all proportions. The reason the method can be used is that the rate of diffusion is slow. Pronounced mixing of the gases does take place, however, both because of diffusion and of the turbulence created when the tracer gas is introduced or shifted. Diagrams *E*, *F*, and *G* show that for real gases the indication given by the analyzer cannot be expected to show that the tracer gas is simply present or absent. Some always will be present. The direction toward the leak can be determined readily, however, by the change in readings with time during the shifting of the column as described later.

After the direction toward the leak is determined at a joint near the center of the affected stop-joint-to-stop-joint section, the procedure is repeated at a joint near the center of the affected half and so on until the affected joint-to-joint section is located.

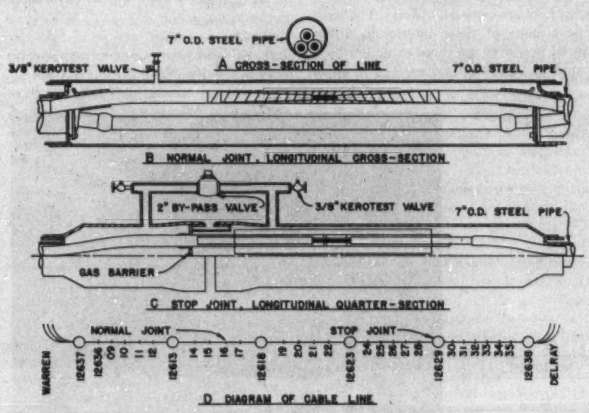


Figure 1. Structural features of cable line for which test was developed

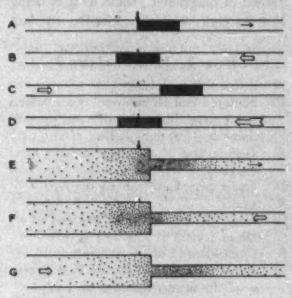


Figure 2. Principle of step 2

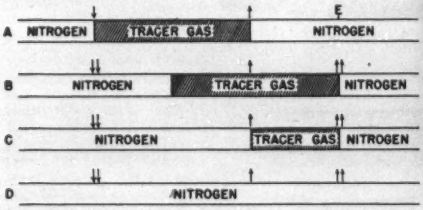
- Column of tracer gas idealized as non-diffusing
- Point of entrance of tracer gas
- Sampling point (same point as above)
- Direction toward leak
- Direction of force that pushes column a distance equal to half its length
- Direction of force that pushes column a distance equal to its length
- Column of real tracer gas showing diffuse edges

The principle of step 3, illustrated in Figure 3, is similar to that of step 2. The tracer gas is introduced at one end of the affected joint-to-joint section at a rate to compensate exactly for the leak the same as in step 2. The quantity introduced is sufficient to displace all of the nitrogen in that section. As the tracer gas is introduced the front of the column migrates toward the leak. When the leak is reached, the tracer gas, instead of nitrogen, leaks out, thus disposing of the excess. At the same time the introduction of the tracer gas is completed,

therefore, a column of the tracer gas extends from the joint at which it was introduced to the leak, and a column of the original nitrogen extends from the leak to the joint at the opposite end of the section, designated by E in diagram A. Gas is then withdrawn rapidly at E and is metered and analyzed. Simultaneously nitrogen is introduced at the other end of the section at a rate equal to the rate of withdrawal plus the rate of the leak. The first gas metered is nitrogen. Its volume is indicative of the distance from the leak to E. The position of the tracer column is then as shown in diagram B. From the time the column of tracer gas is shifted from its position, as in diagram A, until its trailing end passes the leak as shown in diagram C, some of it is being lost through the leak. Finally, the last of the tracer gas is removed from the section as shown in D. The quantity of tracer gas metered plus the quantity that escapes while the column passes the leak is indicative of the distance to the leak from the joint at which the gas was introduced.

Figure 3. Principle of step 3

A Before shift of column
B Nitrogen column removed; no nitrogen lost through leak. Part of tracer gas column being lost through leak
C Trailing end of tracer gas column passing leak. Tracer gas no longer being lost
D Tracer gas column entirely removed
↑ Leak
↓ Tracer gas introduced at one end of affected section at same rate as leak
E Opposite end of affected section
↑↑ Gas withdrawn at location E at a rate materially greater than that of ↑
↓↓ Nitrogen introduced at a rate equal to ↑ + ↑



Interferences

As is evident from the foregoing principles, a sensitive means for determining pressure changes is necessary for all three steps of the test. A leak of one tankful of nitrogen (200 cubic feet at atmospheric pressure) per day in an average stop-joint-to-stop-joint section of the gas pressure cable line would result in a loss of pressure of approximately 0.1 pounds per square inch per hour. During the normal working hours the pressure increases as the result of load and ambient changes at an average rate in excess of 0.25 pounds per square inch per hour. For this reason the differential pressure change across a stop joint, instead of the change in gauge pressure, is used to determine the change in pressure resulting from a leak in any section.

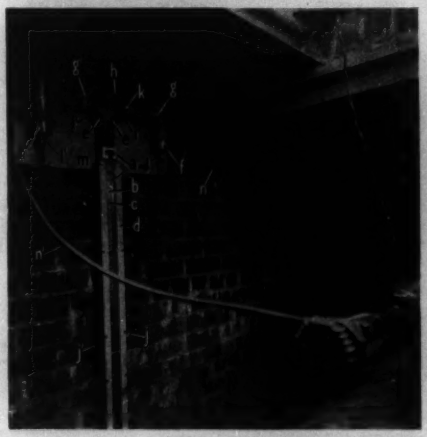
This differential pressure is measured by means of a differential oil-manometer. Obviously this method can be used only if at least one stop-joint-to-stop-joint section is leak free.

Not all differential pressures across a stop joint are caused by leaks. The most conspicuous of these interfering differential pressures are those exhibited by a rapid up-and-down motion of the oil in the differential manometers. The oil levels in the legs of the manometers have been observed to surge up and down several times per minute in amplitudes as high as fourteen inches. Usually these surges, hereafter called waves, are not present. Inasmuch as large waves have been observed to develop within two hours it is necessary to eliminate the interference they introduce in the measurement of pressure changes resulting from leaks, which may be only a few hundredths of an inch of oil per minute. Fortunately the interference caused by the wave motion, which is rhythmic in character, may be almost entirely eliminated, as will be described later. More troublesome to eliminate than the effect of the rhythmic motion of the waves is the effect of entrapping a wave crest as the stop-joint by-pass valve is closed. Only as the excess gas from the wave becomes distributed evenly in the section in which it was caught does the average differential pressure become constant.

This differential pressure may amount to several inches of oil and as much as an hour may be required for equilibrium to take place. The change in the differential pressure resulting from entrapment of a wave may be distinguished from that caused by a leak by the

Figure 4. Manometer designed to be left unattended

a—Manometer proper, 250 pounds per square inch working pressure, gland packed
b—Adjustable scale of (a) reading upward and downward from 0 at midscale, a total of 50 inches
c—Transparent plastic protective window over the glass manometer tube
d—Housing for c
e, e^1—Tubing connecting legs of manometer to antisplash bulbs
f, f^1—Through manometer valves g, g^1
h—Manometer by-pass valve
i, i^1—Metal tubing in parallel with an extending below bottom of glass manometer tubing
j, j^1, k^1—Caps over openings through which oil is introduced into tubing and manometer to the level (j — j^1)
l, l^1—Caps on drains to antisplash bulbs
m—Mounting panel
n, n^1—Tubing connecting to test valves on each side of stop joint



fact that whereas the rate of change diminishes with time in the former case, it remains constant in the latter.

Another source of differential pressure which interferes with those caused by leaks is that caused by unequal rates of heating and cooling in different parts of the line. For example, in the line on which the development was carried out, the section at one end is not buried as deeply as the rest of the line. This section, as might be expected, apparently heats up more rapidly than the rest of the line during the day and cools more rapidly at night. The rates at which the resulting differential pressures change at the stop joint between this section and the rest of the line are at times as great as those that would result from a leak of approximately half a tankful of nitrogen per day. This interference is eliminated, when necessary, by extending the test period over a 24-hour interval during which time the differential pressure that builds up in one direction during the daytime is cancelled by the differential pressure that builds up in the other direction at night.

For steps 2 and 3 there is another interference that results from the mixing of the tracer gas with the nitrogen. As previously stated, considerable mixing takes place by turbulence and diffusion as the tracer gas is introduced through the narrow-throated test valve into the joint. Further mixing takes place as the column of tracer gas is pushed along the pipe, as might be expected because the three cables within the pipe make the gas space

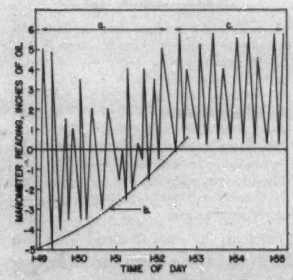


Figure 5. Typical manometer readings taken on one leg of manometer immediately after closure of stop-joint by-pass valve while wave conditions exist

a—Irregular and diminishing waves occurring immediately after by-pass was closed
b—Changing pressure apparently resulting from entrapment of a wave
c—Rhythmic motion—two larger waves followed by a smaller one

irregular in shape. The means by which the interference caused by the mixing of the gases is minimized is described later.

Procedure and Equipment

Recommended practice is to test the cable line for the presence of leaks periodically, say once every six months, by means of "Step One." Any leaks present are thus located as to the affected stop-joint-to-stop-joint or terminal section. Step 2, if required, and step 3 are then applied whenever it becomes expedient to do so. Inasmuch as the tracer gas must be introduced no faster than the rate of the leak, there is a minimum size of leak that is economically expedient to attempt to locate by means of steps 2 and 3.

Because the successive steps progressively narrow the region in which a leak need be sought, use of auxiliary tests may eliminate the need for carrying the systematic, stepwise procedure to completion. These auxiliary tests include patrolling the line for obvious mechanical evidences, and the usual methods for seeking gas leaks such as listening, and applying soapsuds or water around joints at manholes.

The stepwise procedure is preferably carried out with the line in normal operation. In order to minimize thermal gradients, the line should not be shut down during leak tests, unless it is desirable in order, either to safeguard the cable, or to close the by-pass valves on the affected section for the purpose of saving the gas. For leaks so large that shutdown of the line is necessary, the effect of the differential pressures caused by thermal gradients is negligible as compared with those caused by a leak.

Description of the stepwise procedure and the equipment required is given in the following sections.

Step 1

For routine inspection of the line for leaks, according to step 1 a manometer is placed in operation across each of the stop joints that connect the several stop-joint-to-stop-joint sections. The by-pass valve on each of these stop joints is closed and the differential pressures that result between the several sections are allowed to accumulate simultaneously. Readings are taken on each manometer several times during the course of the first day of the test and again several times on the second day. Between readings the manometers are left unattended unless the rate of change of the differential pressure is so great that the range of the manometers

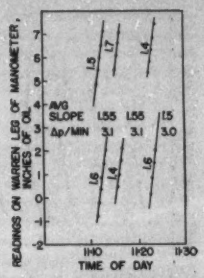


Figure 6. Graphical method for determining the rate of change of the differential pressure at a stop joint (when wave conditions are simple)

might be exceeded before the next reading period. Tests at the terminal stop joints are usually made separately, on the line under discussion, because the volumes of the gas space in the terminal sections are so small that the presence of any significant leak is easily detectable within such a short time that nothing would be saved leaving the manometers unattended.

The manometer assembly used, shown connected across a stop joint in Figure 4, was designed to be left unattended. Inasmuch as all tests are run with the line in operation it was necessary that some means be provided to enable those responsible for the operation of the line to be warned should a sudden leak develop, such as by mechanical accident, while the manometers are in operation unattended. As previously stated, the stop-joint by-pass valves are kept open during normal operation. The pressure of the entire line is then continuously indicated by instruments connected to the gas space of the terminal sections. Closure of more than one stop-joint by-pass valve, which of course is necessary if several manometers are operated simultaneously, isolates part of the line from the instruments except for the flow that would take place through the manometer tubing should the range of the manometer be exceeded. Because even the best glass tubing is not infallible, it seemed advisable to shut off the manometer proper while the assembly is left unattended. In order to provide a mechanism for communicating across a stop joint any excessive pressure differential that might develop while a test is in progress and the manometer proper is shut off, metal tubing i, i^1, Figure 4, was placed in parallel with and extended slightly below the manometer tubing. This metal tubing is filled with oil to approximately the same level as that in

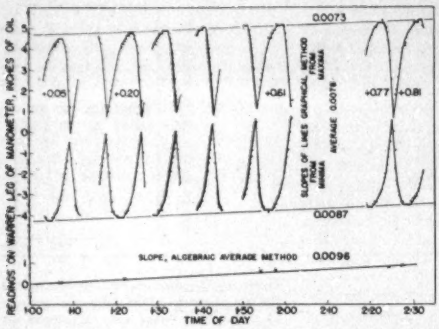


Figure 7. Comparison between graphical and algebraic average methods for determining the rate of change of the differential pressure at a stop joint when wave conditions are complicated

Note: Rate of change of the differential pressure, inches of oil per minute, equals twice the slopes of the lines

the manometer. The oil prevents transfer of gas across the stop joint until a differential pressure of somewhat over fifty inches of oil is built up, whereupon the oil is discharged into one of the anti-splash bulbs, f, f^1, leaving a free path for gas transfer.

Operations preliminary to the use of the manometers consist in attaching them across the stop joints as in Figure 4, bringing the manometers to line pressure, and testing them to insure that the metal tubing contains sufficient oil and that all connections are leak free. Before bringing the manometer to line pressure the manometer by-pass valve, (h) is opened. One of the two test valves on the joint is then opened slowly, bringing the manometer to line pressure. The connection at the other test valve is next cracked slightly in order to allow displacement of the air within the manometer. The connection is then tightened until the escaping gas makes only a barely audible hiss after which valve (h) is turned cautiously toward the closed position until the oil levels in the manometer become displaced nearly to full scale, thus showing that the parallel metal tubing, in which the oil levels are not visible, contains sufficient oil. Valve h is then opened, the connection is tightened, and the test valve on the stop joint is closed. To determine whether any leaks exist in the connections valve h is next closed. Because the volume of the gas space in the manometer and connections is small, the presence of any leak is quickly evident by the shifting of the oil levels. If any are present their locations are sought by means of soapsuds and are eliminated. Both test valves on the joint are then opened. In general all manometers are installed and the tests herein described are made before the reading period is started.

To start the reading period, the stop joint by-pass valve is closed and the time is noted. Readings are taken over an interval of approximately 15 minutes, whereupon—unless the levels are changing at a rapid rate—the manometer valves are closed. Initial readings are then taken on other manometers in turn. Both the manometer by-pass valve, (h) Figure 4, and the stop-joint by-pass valve are kept closed between readings. Subsequent reading periods may usually be reduced to approximately five minutes.

In the absence of waves the taking of the readings is conventional. In the presence of waves it is convenient to read only one leg of the manometer. Simultaneous readings are taken of the time and of the oil level at one or more characteristic parts of the wave, usually the maximum height to which the oil level rises in one leg of the manometer and the minimum height to which it falls.

Typical wave behavior is shown in Figure 5 in which the points representing successive readings of maxima and minima are connected by lines. This figure also illustrates the phenomenon of entrapment of a wave crest, previously mentioned as one of the interferences that must be overcome. Apparently wave forms may vary from simple sine waves to complicated forms resulting from interference and reinforcement phenomena. All waves, however, appear to go through a repetitive cycle, the recognition of which permits the effect of the wave to be practically eliminated.

For simple waves, in which the amplitudes of successive waves are essentially constant, the effect of the wave is conveniently eliminated graphically, as in Figure 6. In this figure the points representing successive readings of the maximum height to which the oil rises in the manometer are connected by one straight line and the points representing successive readings of the minimum height are connected by another. The average slope of the two lines gives a measure of rate at which the differential pressure across the stop joint is changing. Because the readings are taken on only one leg of the manometer, the slope of the line is multiplied by 2 to obtain the rate of change of the differential pressure in inches of oil per minute, designated in Figure 6 as Δp/min.

For cases in which the differential pressures are accumulated on the manometers over a period of several hours, the wave form may change. If the wave form changes the effect of the waves is best eliminated by determining the algebraic average of all maxima and minima readings that are taken over a complete cycle. Using this average and the time of the midportion of the cycle to establish a single point, the slope of the line that connects several such points is determined.

Usually a complete cycle may be recog-

Figure 8. Capillary regulator

A, B, C—18-8 stainless steel capillaries decreasing in resistance to gas flow from A to C
D—By-pass tubing
a, b, c—Shut-off valves to A, B, and C
d—Valve to regulate flow of gas through D
E—Exhaust regulator
F—Chamois filter
G—Gauge to show pressure on line side of capillaries
H—Connection to line
I—Connection to tank regulator
J—Mounting panel

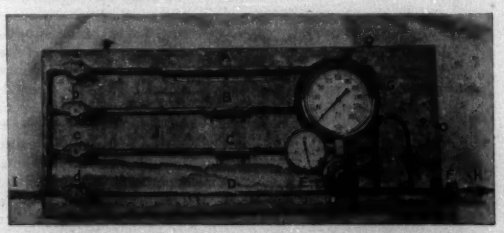


Figure 9. Suspension of cell and batteries of analyzer in manhole to minimize temperature changes during operation

nized by inspection of the manometer readings. Figure 7 gives an example of a case, the most complicated thus far experienced, in which recognition, without the plotting of the data, would have been difficult. In this figure the points representing successive readings of the maxima are connected, and likewise those representing the successive readings of the minima. The marked reinforcement and interference of the waves, and the approximate three-minute lag from the time the displacement of the oil level was greatest in negative direction until it became greatest in positive direction are evident. The midpoints of each time interval over which a complete cycle of readings was taken and the algebraic averages of the readings for each cycle are given by the X's at the bottom of the figure. A line connecting these X's has a slope of 0.0096 inch per minute as shown.

For the illustration in question it is evident that the character of the wave did not change over the period of these readings. For cases in which it is unnecessary to combine such readings with others taken when the wave form is different, it is more convenient, as previously stated, to effect a graphical solution. The graphical solution for the complicated set of data in question is included also in Figure 7. There a line is drawn through the points representing the strongest maxima, and another through the points representing the strongest minima. The average slope of these two lines agrees well with that determined from the algebraic averages.

Elimination of the effect of the rapid up and down wave motion leaves a more or less slowly changing differential pressure across a stop joint which may be the result of a leak, thermal inequalities, or the entrapment of a wave crest. That resulting from a leak is of a practically constant rate; that from thermal inequalities may be nearly constant over several hours, but during the course of a day changes rate and direction; that caused by the entrapment of a wave crest diminishes with time after the first several minutes as previously illustrated in connection with Figure 5. The effect of the latter two is therefore minimized by extending the test period. In practice, the average rates at which the differential pressures change when the line is leak-free are determined for test periods of various durations, and these rates are used in interpreting results.

The direction toward a leak is, of course, the direction toward which the oil column in the manometer rises. The rate, in cubic feet per day, is the product of the rate at which the differential pressure, in inches of oil (specific gravity 0.885) per minute, changes, multiplied by the volume of the section in cubic feet, by the conversion factor 3.13 which converts inches of oil per minute to atmospheres per day.

With several manometers in use simultaneously it is obvious that if a leak is present in any section that is between two manometers, the rate of differential pressure change at both should be identical. Any lack of agreement between the two, beyond the probable limits due to thermal inequalities, is evidence that leaks exist in more than one stop-joint-to-stop-joint section. Procedures for locating multiple leaks, whether in different stop-joint-to-stop-joint sections, the same stop-joint-to-stop-joint section but different joint-to-joint sections, or in the same joint-to-joint section, have been worked out in principle but are beyond the scope of this paper. Likewise procedures have been developed for locating leaks that are so large that the line must be shut down and the stop-joint-by-pass valves closed to conserve gas. All of these procedures, which are too detailed to be included but which are based upon the same principles as the general procedure described, are available to anyone interested.

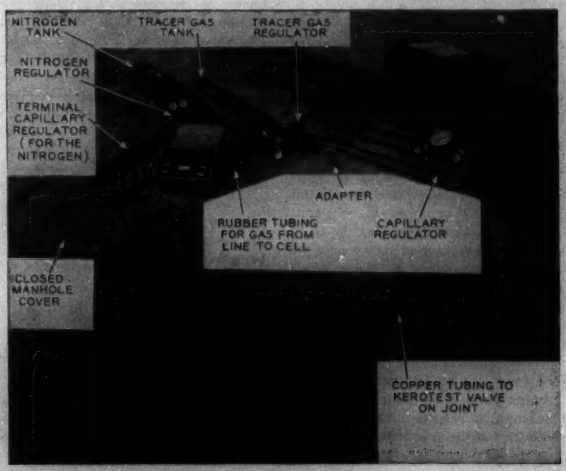
Step 2

Location of a leak to the affected joint-to-joint section, by the method described in this paper, requires

1. A supply of tracer gas.
2. Equipment for introducing the tracer gas into the line at a rate equal to the rate of the leak.
3. Nitrogen supply and other apparatus for shifting the column of tracer gas along the line.
4. An analyzer for differentiating between the tracer gas and the nitrogen with which the line is normally filled.

The tracer gas selected and used to date is a mixture of five per cent by volume of carbon dioxide in nitrogen. Carbon dioxide was chosen in preference to other gases because its sparking potential is

Figure 10. Assembly of equipment at a manhole at which tracer gas is introduced for step 2



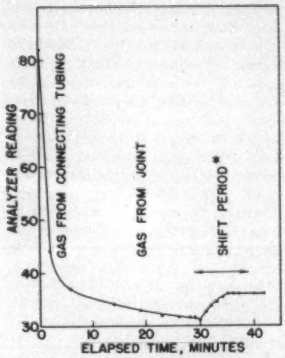


Figure 11. Typical data during the shifting of the column in step 2 showing leak to be in direction from which the nitrogen was introduced. Volume of tracer gas introduced—73 cubic feet

*—During shift period 36 cubic feet of nitrogen introduced at the adjacent joint in one direction from the analyzer and withdrawn at the adjacent joint in the opposite direction

even higher than that of nitrogen, because it is obtainable commercially in fairly pure form, and because its use presents no hazard to either personnel or equipment. The proportion of carbon dioxide to nitrogen was kept as low as practical because even the purest carbon dioxide that is commercially available often contains more moisture than is permissible in the cable-gas according to specifications.

In order to minimize the number of tanks of gas required for storage, it was considered more expedient to make up the mixture of tracer gas as required, in the tanks ordinarily used to store the nitrogen, instead of purchasing and storing the mixture. For making up the mixture a manifold having ten places for nitrogen tanks and one place for a carbon dioxide tank is used. It is required that the moisture content of the carbon dioxide be not in excess of 0.35 grams per cubic meter at atmospheric pressure and that the nitrogen have a dew point, at 2,000 pounds per square inch or greater, not higher than 0 degrees Fahrenheit. Thus the moisture specification of not exceeding 0.0025 per cent water by weight in the gas mixture is readily met. Usually step 2 requires no more than four 200-cubic foot tankfuls of the tracer gas but inasmuch as step 3 requires a considerable number it may be convenient to prepare those required for both steps at the same time.

The equipment for introducing the tracer gas at a rate equal to the rate of the leak consists of a tank of the gas, a nitrogen regulator, a capillary regulator, a differential oil manometer, and the necessary connecting tubing. The nitrogen regulator is of conventional type, having a pressure gauge on both the high and the low pressure side. It should furnish regulation up to 300 pounds per square inch. The capillary regulator is shown in Figure 8. It consists of three capillary tubes mounted in parallel within protective covers, as indicated by *A*, *B*, and *C*; bypass *D* in parallel with the capillaries; valves (a), (b), (c) and (d) controlling *A*, *B*, *C*, and *D* respectively; exhaust regulator *E* mounted beyond the line end of the capillaries and containing gauge *G* on its high pressure side; chamois filter *F* to protect the capillaries from dust that may be in the line; connections *H* and *I* to the line and to the tank regulator respectively, and mounting panel *J*. The capillaries consist of *18-8* stainless steel hypodermic needle tubing of various sizes progressing from the smallest at *A*. The manometer has been previously described.

To effect the desired shift of the column of tracer gas it is necessary to introduce a given volume of nitrogen at a joint in one direction from the column and withdraw an equal volume at another joint in the opposite direction. The equipment required to introduce the nitrogen consists simply of a tank of nitrogen, a tank regulator, and connecting tubing. The volume introduced is measured with sufficient accuracy by the change in volume indicated by the volume scale of the high-pressure gauge. The equipment used to remove the required volume of nitrogen consists of an iron-case gas meter, a valve, and connecting tubing. The meter is a so-called *20-B* size rated at 350 cubic feet per hour at a pressure differential of 1/2 inch of water and 700 at two inches.

The dial can be read to 0.1 cubic feet, a feature necessary for step 3, with which it also is used. The analyzer was made specifically for the job after it was found that a commercial analyzer, around which the procedure was built, was too sensitive to temperature changes to be used by field personnel. With other commercial analyzers, either too large a flow of gas was required for their operation, or too large a current flow was required to permit operation by dry batteries as the current source. At the time the method was being developed, instrument makers were too busy with war work to be bothered with making a suitable instrument.

The analyzer operates on the usual principle which depends upon the difference in thermal conductivity of gases. A unique feature is a plug-type cock which in one position permits the reference gas, dry nitrogen, to contact both elements while the gas to be analyzed is exhausted, or in the reverse position permits the reference gas to contact only one element while the gas to be analyzed contacts the other. Thus the zero point of the analyzer may be checked quickly and easily. The active elements, made from General Electric Mazda *24X* telephone light bulbs, are matched as closely as possible both for resistance and for change in resistance with temperature change. The effect of temperature change is further minimized by suspending within a manhole during use, both the brass cell, which contains the active elements, and the batteries as shown in Figure 9. Nitrogen for reference gas is supplied through a nitrogen regulator and orifice, which conveniently may be one capillary of the terminal cap-

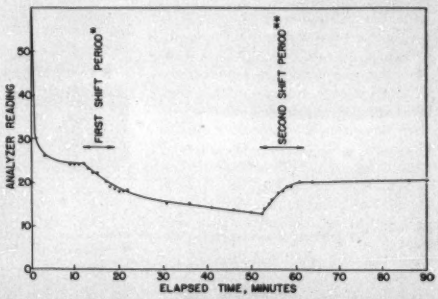


Figure 12. Typical data showing leak to be in the direction from which the nitrogen was withdrawn during the first shift period of step 2. Volume of tracer gas introduced—32 cubic feet

*—During first shift period 16 cubic feet of nitrogen introduced at the adjacent joint in one direction from the analyzer and withdrawn at the adjacent joint in the opposite direction
**—During second shift period (confirmatory test) 32 cubic feet of nitrogen introduced and withdrawn at joints in directions reversed from those referred to above

illary regulator to be described in connection with tests made at terminals in step 3.

The first operation in step 2 is to assemble the equipment as shown in Figure 10 with the tank of tracer gas connected in series with its tank regulator and the capillary regulator. The latter is connected in turn to the joint that is near the midpoint of the affected stop-joint-to-stop-joint section. A manometer is connected across a stop joint that is between the affected section and a leak-free section. The rate at which the tracer gas is fed is adjusted in such a way that the oil levels in the manometer remain constant. To do this the tank regulator is adjusted until its low pressure gauge reads a few pounds higher than the line pressure as shown by the line pressure gauge on the capillary regulator. Through choice of one or more capillaries on the capillary regulator and the differential pressure across them, the rate of introduction is controlled easily over a wide range. The volume introduced is, for the line in question, from 30 to 60 cubic feet at atmospheric pressure, depending principally upon the extent to which thermal gradients effect a change in differential pressure at the stop joint containing the manometer.

During the introduction of the tracer gas the analyzer is assembled with the suspended parts clamped to one of the two manhole covers as shown in Figures 9 and 10, and the equipment for shifting the position of the gas column is assembled at the adjacent joints. The analyzer is placed in operation with the handle of the plug valve, Figure 10, protruding above the closed manhole cover, set in such a position that nitrogen from the tank comes in contact with both elements. The bridge arms of the analyzer are adjusted to give a zero reading of the galvanometer under these conditions. After the desired volume of tracer gas has been introduced the manometer valves on the stop joint, some distance away, are closed. The valves on the capillary regulator are then closed, and the supply connection to the capillary regulator is replaced by the adapter, tubing from which connects to the port on the cell at which gas to be analyzed is introduced. Valve "a," Figure 8, controlling the smallest capillary on the capillary regulator, is then opened and the handle on the plug cock is reversed, allowing a slow stream of gas from the joint to flow through the analyzer. After a few minutes the column of tracer gas is shifted in a selected direction as described previously.

Figure 14. Arrangement of equipment for introducing tracer gas for step 3. Reference manometer is at the same manhole as gas-feeding equipment only in specific cases

A, B, C, D—Tanks of gas mixture
E—Manifold
a, b, c, d—Manifold valves making connections to A, B, C, and D respectively
F—Regulator attached to manifold
G, G¹, M—Gas-feeding line
J—Capillary regulator
H and I—Check valve and by-pass around H (better placed in M)
N—Manometer
L, O, P—Connections, and valve joining left leg of manometer to gas feeding line on one side of stop joint
Q, R, S—Connections, and valve joining right leg of manometer to opposite side of stop joint

Figures 11 and 12 show the manner in which the analyzer readings are interpreted to show the direction toward the leak. In both figures the analyzer readings drop rapidly at first as the undiluted tracer gas is exhausted from the connecting tubing. A slow decline then follows as the column drifts toward the leak. In this connection it should be pointed out that if for proper operation the analyzer requires a rate of flow more rapid than the leak the column will drift back toward the analyzer and may obscure the indication. Upon the deliberate shift of the column the analyzer reading increases suddenly, as shown in Figure 11, provided the leak is in the direction from which the nitrogen was introduced. Conversely, the analyzer reading decreases suddenly, as in Figure 12, if the leak is in the direction from which the nitrogen was withdrawn. In the latter event a confirmatory test is made by shifting the column in reverse direction. For this second shift the volume of gas introduced at one joint and withdrawn at the other is equal to the volume of tracer gas originally introduced. During the second shift the analyzer reading increases sharply, thus confirming the direction toward the leak.

Before similar tests are made near the midpoint of the part of the stop-joint-to-stop-joint section in which the leak has thus been indicated, it is necessary to purge the line of traces of the carbon dioxide. To do this the exhaust valve on the capillary regulator is opened and readings are continued. Typical data are shown in Figure 13. After most of the tracer gas has thus been purged and the analyzer readings approach zero, nitrogen is introduced at one or the other of the adjacent joints, thus shifting the column of the residual gas. The purpose of the

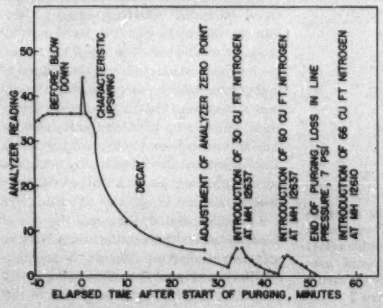
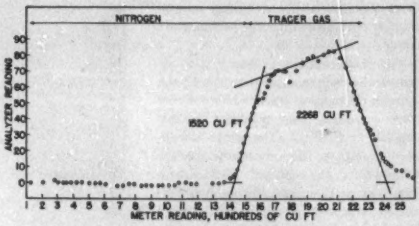


Figure 13. Field data representing the purging of the tracer gas from the line

shifting is of course to center the remaining tracer gas around the valve through which the gas is being withdrawn. Such shiftings, if in the right direction, cause temporary increases in the analyzer reading. If in the wrong direction, as indicated by a sudden drop in readings, nitrogen is introduced from the opposite direction. Purging is continued until the analyzer reading drops to zero and remains at zero as the nitrogen is introduced from each side. The section is then brought up to normal pressure with nitrogen, and the test is repeated at the other joints as described previously, thus locating the leak to the affected joint-to-joint section.



Initial Meter Reading 121 Cubic Feet

Figure 16. Graphical estimation of volumes of gas columns

	Volume Nitrogen Cu Ft	Volume Tracer Gas Cu Ft
Metered	1520-121=1399	2268-1520=748
Unmetered through analyzer	0.1 cfm×211 min=21	0.1 cfm×102* min=10
Through leak		0.51 cfm×121* min=62
Total	1420	820

Note: Time intervals marked * would have been identical if rate of metering had been constant.

Step 3

In general the equipment required for step 3 is the same as that required for step 2. The procedure is similar in many respects. Tracer gas is introduced at a rate to compensate for the leak, utilizing a reference manometer at one of the stop joints of the section involved, as described in step 2. The gas is introduced at one end of the affected joint-to-joint section. Introduction is continued until the volume introduced is approximately five per cent in excess of the volume of gas in that section. The rate of introduction can be so accurately controlled that the presence of an operator is required only a few hours a day after the first day. For leaks having rates much greater than a tankful per day a manifold is required at the feeding station as shown in Figure 14. This figure shows the arrangement of the feeding equipment in a splicer's shanty for a case in which the gas is introduced at a stop joint that can be used for attachment of the reference manometer. Usually, however, the manometer will be in a manhole at the reference stop joint some distance from the feeding equipment.

After the required volume of tracer gas has been introduced the next operation is to shift the columns. Before discussing the equipment and procedure required for this operation, it is desirable to recall that the operation consists of the following steps:

1. Removing rapidly the entire gas content of the joint-to-joint section from one end of it.
2. Metering and analyzing this gas as it is removed.
3. Introducing nitrogen at the other end of the section at such a rate as to compensate both for the rate of removal and the rate of the leak.

During the shifting of the column it is essential that a means of instantaneous communication be provided between the person watching the reference manometer and the one introducing the nitrogen. Unless these two persons happen to be close together, telephone or radio service is required.

Preparations for shifting the column of gas consists of the following steps:

1. Replacing the capillary regulator at the feeding end of the affected section by a needle valve. During this change the feeding of the tracer gas is not interrupted longer than is necessary.
2. Replacing all but one of the tanks of tracer gas on the manifold by tanks of nitrogen.
3. Attaching the capillary regulator, gas meter, and analyzer in position at the end of the section from which the gas is to be withdrawn.

The arrangement of this equipment is similar to that used in step 2 except for the position of the gas meter which is attached to the exhaust valve of the capillary regulator as shown in Figure 15. The analyzer is set in operation, with the plug cock in test position, allowing nitrogen to flow through both elements, as previously described. Preparations are completed by stationing operators at the three locations involved, namely the joint at which the gas is to be removed, the joint at which the feeding of the tracer gas has been conducted, and the reference manometer, which, as previously stated, is at a stop joint that separates the leaky stop-joint-to-stop-joint section from a leak-free stop-joint-to-stop-joint section. The operations described in the next two paragraphs are carried out simultaneously.

At an appointed time, gas from the joint to which the capillary regulator is attached is admitted through the smallest capillary A, Figure 8, to the analyzer, the plug cock of which is then turned to the

Figure 15. Arrangement of equipment for withdrawing, metering, and analyzing the gas during step 3

"analyze" position. The analyzer and meter readings and the line pressure are noted, after which the exhaust valve is opened gradually over the course of a few minutes until the gas is being withdrawn at a rate sufficient that the entire content of the leaky joint-to-joint section will be removed in approximately four to six hours. This rate is maintained constant during the column-shifting operation. During this operation simultaneous notations are made every few minutes of the time, meter and analyzer readings, and the temperature of the exhausted gas. Occasionally the differential pressure between the meter and the atmosphere is noted on the manometer that is shown mounted on the meter, Figure 15. Also the pressure at the entrance to the capillary regulator is noted. This pressure reading is used with a calibration curve in determining the volume of gas that escapes through the analyzer.

At the appointed time referred to in the preceding paragraph the supply of tracer gas is shut off at the feeding manifold and a tank of nitrogen is turned on. At the reference manometer, a displacement of the oil levels results from the removal of gas through the capillary regulator. Information concerning the displacement is communicated to the operator at the feeding manifold, whereupon the rate of introduction of the nitrogen is adjusted to keep the manometer balanced.

The first gas that is metered and analyzed consists, as explained in the principles, of the nitrogen that was originally in the section. This nitrogen is followed by the tracer gas, and this in turn is followed by the nitrogen that was introduced as the shifting of the column was started. After all the tracer gas has been removed, the exhaust valve is closed and the final pressure of the line is noted. This may be slightly different from the original pressure, owing to loading and ambient changes. Care is taken to purge from the adjacent ends of the adjoining sections any of the tracer gas that may have entered these sections during the introduction of the tracer gas or the shifting of the column.

As stated when the principles were discussed in connection with Figure 3, the volume of the column of nitrogen that was in the section before the column was shifted is a measure of the distance from the valve at which it is removed to the leak, and the volume of the column of tracer gas is a measure of the distance from the valve at which it was introduced to the leak. As also stated, a part of the tracer gas is lost as the column passes the leak. The volume lost is the product of the rate of the leak, which is known from "Step One," and the time required for the column to pass the leak. If the gas is withdrawn at a constant rate the time required for the column of tracer gas to pass the leak is

$$\frac{v_t}{v_t+v_n}\times t$$

where

v_t = metered volume of tracer gas
v_n is the metered volume of nitrogen
t is the total time required to meter both columns

In addition to the gas lost through the leak, a part passes through the analyzer. The rate at which this gas escapes is obtainable by means of a calibration curve from the pressure indicated by the gauge on the capillary regulator with the exhaust valve open. The product of this rate by the time required for each gas to be metered gives the respective values of unmetered gas passed through the analyzer. These unmetered volumes are added to the metered volumes as subsequently described.

Because the line of demarcation between the tracer gas and nitrogen is not absolutely sharp, the volumes of the respective metered gases and the times required for metering them may best be solved graphically. Figure 16 shows field data for the simulated leak to which reference has been made. The analyzer readings remained essentially 0 from the initial meter reading of 121 cubic feet to approximately 1,400 cubic feet. The analyzer readings then rose sharply as shown, then more gradually. Next, they dropped sharply and then more gradually toward 0. Interpretation of where one column ends and the other begins may vary slightly for different interpreters. For use by field personnel, it seemed desirable to adopt the following instructions which are illustrated in connection with the figure:

1. Draw a line tangent to the curve through the point of inflection, that is, through the greatest number of consecutive points in the major rise in the curve that indicate the appearance of the tracer gas.

2. Draw a similar line through the points that indicate the disappearance of the tracer gas.

Figure 17. Terminals showing sampling valves immediately beneath the potheads

Figure 18. Terminal capillary regulator and analyzer connected as used for terminals

3. Draw a third curve across any flat part of the points representing the maximum readings obtained. This curve, which is not necessarily a straight line, will generally slope upward because the trailing remainder of nitrogen in the irregular spaces between the cables is gradually swept out as more of the tracer gas passes by it. Variations in composition of successive tankfuls of tracer gas may also contribute to deviations from a straight line. The curve so drawn intersects both with the line showing the appearance and with that showing the disappearance of the tracer gas.

4. Find the midpoint between the intersection and zero analyzer reading on the line representing the appearance of the tracer gas. The meter reading corresponding to that midpoint is taken as the meter reading at the end of the nitrogen column and at the beginning of the tracer-gas column. (In the case illustrated in Figure 16, 1,520 cubic feet.)

5. Find the midpoint between the intersection and zero analyzer reading on the line representing the disappearance of the tracer gas. That midpoint is taken as the meter reading at the end of the tracer-gas column, or 2,268 cubic feet.

6. The difference between the reading at the end, and the reading at the beginning of the tracer gas column is the volume of tracer gas metered, or 748 cubic feet.

7. The difference between the reading at the end of the nitrogen column and the initial meter reading is the volume of nitrogen metered, 1,399 cubic feet.

After the meter readings corresponding to the appearance and the disappearance of the tracer gas column have been determined, the time intervals necessary for calculating the volumes of the unmetered gas are obtained from a plot of the meter readings versus elapsed time. The sums of the metered and unmetered volumes in the nitrogen and tracer gas columns under the conditions of temperature and pressure at which they were measured are then obtained as illustrated in Figure 16.

The volumes that the gas columns occupied, before the column-shifting operation, under the conditions of temperature and pressure existing in the pipe line are calculated by means of the gas laws. That is

$$V_1 = V_m \frac{P_m}{P_1} \frac{T_1}{T_m}$$

where

V_m = volume of the gas determined as described in connection with Figure 16

P_m = pressure of the gas in the meter = barometric pressure plus the pressure indicated by the meter manometer

P_1 = Average of initial and final pressure of line expressed in the same units as P_m

T_1 = Average temperature of the line, in absolute degrees

T_m = Average temperature of the gas at the meter during the metering of that column in absolute degrees

The gas space in the affected joint-to-joint section is considered to extend from the test valve on one joint to the test valve on the other. In the line in question these valves are not symmetrically spaced but are located near the ends of the joints. Thus both the column of nitrogen and the column of tracer gas consist partly of the space from valve to end of joint and partly of space within the pipe. The volume of the space within the joint is subtracted from the volume of gas calculated as previously described. The difference is the volume of the column of gas within the pipe. This difference, for each column, is then divided by the specific volume of the gas space within the pipe line to give the distance to the leak from the end of the joint at which the gas was removed and the end at which it was introduced, respectively. Thus the position of the leak is indicated by two independent measurements. Excavation is then made in the region so indicated and the exact spot is located either by means of soapsuds or by covering the pipe with water.

Terminals

A few important differences exist between locating leaks in terminals and locating them in joint-to-joint sections by means of "Step Three." At the terminals the three cables, instead of being in a single pipe, are contained, as previously stated, in separate tubes called sweeps. The three sweeps at each end of the line run from the terminal stop joints to their respective potheads as shown in Figure 17. Below each pothead is a gas sampling valve in the position shown.

During the feeding operation the tracer gas is fed at the stop joint to which the sweeps are attached and the reference manometer is used at the same stop joint in the manner shown in Figure 14. Instead of the usual excess of five per cent, an excess of approximately 25 per cent of tracer gas, based upon the calculated volume of the gas space, is used.

The gas for metering and analysis is removed separately from the test valve at the base of each of the three potheads. Whereas nitrogen is used at the feeding station to displace the gas removed during the column-shifting operation for joint-to-joint sections, the tracer gas is used for terminal sections. Metering and analysis of the gas from each of the three sweeps is discontinued as soon as the nitrogen column has been entirely removed from that sweep.

Because the volume of the gas space in the sweep per unit length is small when compared with that of the pipe between joints, gas is withdrawn much more slowly in order that an adequate number of analyzer readings can be taken per unit length of sweep. The rate of withdrawal is controlled by means of the terminal capillary regulator shown in Figure 18. This terminal capillary regulator consists of three capillary tubes that occupy the positions indicated by lines *1*, *2*, and *3*. Capillary *1* controls the rate of flow of nitrogen to the cell, and capillary *2*, the rate of flow of gas from the line. These two capillaries are of the same dimensions and, therefore, when the pressure of the nitrogen is adjusted to line pressure the rate of delivery of nitrogen and gas from the line is the same. Capillary *3* serves to conduct most of the gas from the line directly to the meter. Unlike the procedure for the joint-to-joint sections in which the gas required to run the analyzer is exhausted into the atmosphere and its volume is computed from the calibrated rate and the time involved, the gas required to run the analyzer during tests on terminals is metered. The metering is accomplished by reuniting the exhaust

from the line side of the cell with the gas that is shunted through capillary *3*, before the gas is passed through the meter. During the short periods in which the handle of the cell is turned in such a way that nitrogen is passing through both elements in order that the zero point may be adjusted, the gas from the line is being exhausted into the atmosphere and nitrogen from the cell is being admitted into the gas stream entering the meter. Because capillaries *1* and *2* are of the same dimensions, however, no error in meter reading is introduced.

Further differences include the necessary use of the cell and batteries above ground, as shown, instead of in a manhole; the use of a smaller-sized meter; and the necessity for flushing the tracer gas from the terminal section by introducing nitrogen at the test valves on each of the sweeps while withdrawing gas from the stop joint.

Accuracy

Experience with the method is thus far too limited to permit adequate estimation of its accuracy. Figure 19 presents data taken in the last step of the location of an actual leak which, by "Step One," was shown to be in a terminal section. The first of the gas removed from beneath the pothead on the X-phase was nearly half tracer gas. That from the Y-phase was practically all tracer gas. In the Z-phase, on the other hand, the presence of the tracer gas was not indicated until a volume of gas nearly equal to the volume of the gas space in that sweep had been removed. The data were interpreted to mean that the major part of the leak was in the Y-phase, at or beyond the test valve below the pothead and probably in the pothead itself and a secondary leak existed in the X-phase, near or in the pothead. The Z-phase was reasonably leak free. In this connection it should be pointed out that should a given section contain

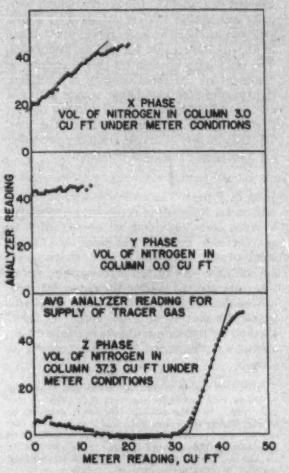


Figure 19. Field data showing location of a leak in a terminal

Principal leak, pothead of *Y* phase
Secondary leak, pothead of *X* phase

more than one leak, the position of only one of the leaks will be accurately located during a single test. Procedures for locating secondary leaks have been devised but are too detailed for inclusion in this paper. In the case of the leak under discussion and treated by the figure, no confirmatory tests were run. The curves were, however, analyzed in some detail to give an estimate of the relative rates of the leaks in the two phases. The positions and relative rates were found, upon shutdown of the line, to be as predicted by the tests.

The only other information concerning accuracy was obtained during the test of the method by means of an artificial leak as has been described in part in several sections of this paper. The total length of the line is nearly seven miles. The length of the section upon which the final step was carried out was slightly over 1,000 feet. The error in the location of the leak as calculated from the volume of the nitrogen, called the primary standard, was 17 inches; as calculated from the volume of the tracer gas, called the secondary standard, the error was 17 feet.

Accuracy is of course dependent upon the exactness with which the temperatures and pressures of the gas, and the volumes of the gas spaces, are known, as well as upon the degree to which the manometer is kept balanced during the feeding and column-shifting operations. For the line in question, the exact temperature of the gas within a given section is not known. The temperature used in the computations is the average of the indications determined at the several thermocouple stations. A source of possible further error arises from the fact that the volume of grease used to lubricate the cable is unknown. Nevertheless, the method appears to be a practical means for locating leaks in gas pressure cable lines and similar types of installations. Its disadvantage of being a slow method for locating small leaks is counter-balanced by the fact that interruption of the service of the line is unnecessary.

Discussion

C. T. W. Sutton (Enfield Cables Limited, Brimsdown, Enfield, Middlesex, England): The first pipe line compression cable was installed in 1932 between Hackney and Walthamstow, London for the Central Electricity Board. The installation of a steel pipe containing high gas pressure was a bold venture in those days because little knowledge was available in Britain of methods of testing, welding, and protecting thin steel tubes laid direct in the ground. Unfortunately, on completion a leak was found to exist, due to a flaw in a tube.

To locate this weakness many methods were investigated including pressure measurements, gas detectors, and so forth. Finally success was obtained by a technique employing principles almost identical to those described in the paper although differing in such details as methods of gas analysis and the type of tracer gas used.

During the initial years of service, leaks developed from time to time caused by electrolysis due to stray current from the d-c third rails, all of which were located to within a few feet by using the tracer or bubble method, described in British patent 421,324.

The location of gas leaks on the Hackney-Walthamstow line was rendered more difficult because only one barrier joint, in the middle of the run, had been installed and the joints were not accommodated in manholes but were buried directly in the ground, and no provision had been made for gas testing at the joints. In spite of these drawbacks leaks of varying magnitudes were quickly and accurately found, excavating being necessary at a few joints only. It was found that apart from the measurement of the actual magnitude of the leak, to estimate the gas flow it was unnecessary to measure gas volumes to any great degree of accuracy as the tracer gas acted merely as an indicator of the direction of the main gas flow. In passing, it is of particular interest to note, that since the gas pressure was first applied to the Hackney-Walthamstow line in 1932 it has never once been released, all leak locations, repairs, and recoating have been carried out with full gas pressure maintained, and never once has there been any doubt as to its electrical efficiency. Also, since recovering the pipe with the "Sandwich Protection" no gas leak has occurred for more than four years.

Our method can be described briefly. The line was shut in at both ends and a small volume of tracer gas, either oxygen or air was injected into a joint and left for a waiting period depending on the magnitude of the leak. The next procedure located the position of the tracer by gas sampling at the

injection joint, pushing the whole gas content of the pipe along in a similar manner to that described in the paper and so, by process of elimination, the leak was narrowed down to a length between two adjacent joints. Alternatively, gas was fed in at one end only, to maintain constant pressure in the pipe. The tracer was put into the line at a joint as already described and an analysis of the gas was taken immediately. If the injection joint was between the gas feeding point and the leak the tracer disappeared quickly while on the other hand, if the injection point was beyond the fault, the tracer remained stationary losing its concentration by diffusion only.

The final operations consisted of injecting the tracer into one of the joints after which it was pushed a known distance towards the leak, and left for a period when the location and the shape of the tracer were determined by sampling the moving gas as before. Finally, a volume of the tracer gas was pushed directly over the leak, a condition recorded by the facts that the tracer had remained stationary during the waiting period and a proportion of its volume was lost. An aromatic volatile liquid injected with the last tracer was found to assist greatly during excavations.

The author mentions leaks of the magnitude of 200 cubic feet per day. We have never encountered leaks of this size in actual practice. They have usually been from 20 to 100 cubic feet per day and we would expect to locate a leak of 200 cubic feet per day on the Hackney-Walthamstow line very quickly.

Experience has shown that leaks of less than 20 cubic feet per day cannot be located by the tracer methods because the gas flow in these instances is very slow and the actual movements masked by diffusion.

New methods using artificial barriers and leaks with differential gauges similar to those described in the paper have enabled leaks of the order of 5 cubic feet per day to be found. It was necessary in these tests to allow for temperature variations, wave motion and so forth as given in the paper, but as already stated, measurements to this degree of accuracy were not necessary for the larger losses. Step 3 in the paper would be impracticable for very small leaks of the magnitude mentioned, as a waiting period of days or even weeks would be required for the gas to flow to the leak.

Finally, the tracer or bubble methods do not readily lend themselves to locating gas leaks on self-contained cables, as the large volume of gas in the joints, compared to that in the cable, creates very serious errors and other means have had to be found.

While we can appreciate the work done by the author in preparing the paper, we consider that apart from certain details little has been added to the information on leak detection which has been available for some time. We can only assume by the remarks in the first paragraph that the author had no knowledge of the methods in use in England, Norway, Denmark, and Germany or of the existing British patent 421,324 and others.

Kenneth S. Wyatt (Phelps Dodge Products Corporation, New York, N. Y.): The author's paper is very timely. It will solve the last problem in the minds of many engineers who would like to use gas in pipe cable. The same problem arose in connection with the original pipe cable in London, in 1931. Some of the best engineers in London were at their wits ends trying to develop a method of locating pinhole leaks in the pipe. The solution was due to D. A. O'Duffey, chief chemist of Enfield Cable Works, which basically was similar to that proposed by Piper.

There has been no leakage whatever of gas in the London cable for nearly five years. The electricity authorities there are now so confident of the practicability of complete tightness of 200 pounds per square inch gas systems that they now specify no gas bottles shall be attached permanently to the line. There is merely a simple coupling provided for adding gas should it be needed.

John D. Piper: The author wishes to thank Sutton for his written remarks, particularly because they provided an opportunity to acknowledge publicly the prior and somewhat similar work described in British patent 421,324. As Sutton surmises, the writer was unaware at the time the paper was submitted that such a patent existed. A mimeograph description of a so-called "Enfield" method for locating gas leaks in compression cables was, however, at hand, reading as follows:

The Enfield Cable Works has developed a method which is well suited for locating slow leaks in the pipe line. In this method the cable remains in service during the entire period of leak location and repair. The completed line has a valve on each of the short sections of large diameter pipe which serve as pipe sleeves for the cable joints. In the Enfield method, a so-called bubble of another gas (either air or oxygen) is introduced into the pipe line through the valve on the pipe sleeve of a cable joint at or near the center of the line. Usually about 50 cubic feet (atmospheric pressure) of the foreign gas is injected to form the bubble. The volume of the gas in the pipe line at operating pressure is of the order of one cubic foot at atmospheric pressure per lineal foot of pipe. Consequently the "bubble" occupies approximately a 50-foot length of the pipe line. Owing to the high gas pressure and the very large ratio of the pipe length to pipe diameter, diffusion of the bubble of foreign gas with the nitrogen in the line is very slow. Hence after being introduced into the pipe line, the bubble gradually drifts towards the leak. By taking samples of gas at joints adjoining that at which the foreign gas was introduced, the direction of the leak from the center of the circuit can be determined readily.

Knowing in which half of the line the leak is to be found, the next step is to introduce a bubble near the center of the faulty half and repeat the tests. The same operation is continued until leak is known to be confined to a section between two cable joints.

After the section in which the leak exists is known, a bubble of gas is injected into one of the two joints on each side of the leak. The "bubble" is then pushed toward the leak by introducing a predetermined volume of nitrogen gas at one end of the line and taking out an equal volume of gas at the other end of the line. From the volume of nitrogen gas removed and added, the position of the foreign gas bubble is known with reasonable accuracy.

The procedure is then reversed, and the foreign gas bubble is returned to its initial position. As the bubble is being pushed past its initial position, the operator at the joint takes out samples of gas at intervals. From the foreign gas content of these samples and the length of the bubble, as determined from the volume of nitrogen introduced and removed at the ends, the volume of the returning gas bubble can be found from a simple plot of gas content against bubble length. If the volume of the returning bubble is the same as that originally introduced, the bubble has not passed over the leak.

On the other hand, if the returning bubble is found to have lost part of its initial volume, the bubble has been over the leak and the leak is known to be somewhere in the section of pipe that was occupied by the bubble on its forward trip. The location is then checked by repeating the test from the other joint. Before excavating a phial of ethyl mercaptan is introduced and pushed along the line to the vicinity of the leak. The odor of this gas is helpful in locating the exact spot.

As thusly described the method could not be used for The Detroit Edison Company's gas pressure cable for the following reasons:

1. Neither oxygen nor air could be permitted in the line.
2. It would be impractical to wait until the bubble drifted to an adjacent joint before the direction toward the leak could be ascertained.
3. Location of the position of the leak between two joints by the loss of volume of a bubble placed over the leak would, unless the operators were exceedingly good guessers, require a huge expenditure of man-hours and dry nitrogen.
4. Ethyl mercaptan or any other sufficiently powerful odorant known could not be tolerated in a gas-pressure cable for reasons discussed in this paper.

Accordingly the leak test method, as described in the paper, was devised specifically for the company's cable and was thoroughly tested. During the period of the development, interest in pipe-contained gas pressure cables became very active in this country. The fact that a practical method had been devised for locating gas leaks that might develop in such cables became known, with the result that numerous inquiries were received not only concerning the principles of the method, which are exceedingly simple, but also concerning the field-learned "know-how" of applying these principles which is rather complex. It therefore seemed advisable to make the information available to all concerned.

In preparing the paper for publication, the author recognized that some of the principles were similar to those described in the quoted excerpt concerning the Enfield method, and also that insofar as manometers are used in step 1 the method resembled a method used by the Bell Telephone Company for locating leaks in telephone cables. Inquiry revealed that the method used by Bell Telephone Company had never been published. No record of the Enfield method was found in the technical literature and thus it was assumed that it was unpublished. Resemblances between the two methods seemed too remote to warrant the making of direct inquiry to the Enfield Cable Works.

The author now knows that the principle of step 2 is almost identical with that described in British patent 421,324 and in Sutton's comments. First knowledge of this fact and of the existence of the patent was obtained through the courtesy of Kenneth S. Wyatt who furnished a photostatic copy of part of the patent. From this copy it was learned that the quoted description of the Enfield method was incomplete insofar as it referred to the part to which objection was raised concerning the necessity for the bubble to reach the next joint before its direction of movement could be ascertained.

Although the author is glad to acknowledge that unknown to him the principle of step 2 had been anticipated, he wishes to call attention to the fact that through the use of the techniques and instruments described in the paper the principles may be safely employed in locating leaks in gas pressure cables where oxygen cannot be used. Pure carbon dioxide could possibly be used if it could be obtained commercially in an adequately dry state. This would reduce the requirements for the gas analysis equipment. So far as the author is aware no source of

adequately dry commercial carbon dioxide exists and if it did its solubility in oil might be troublesome. For this reason the low concentrations described and a sensitive analyzer are recommended.

The principle of step 3, although utilizing the principle of slow diffusivity of compressed gases in long slender vessels—a principle which, by the way, anticipates the principle even of British patent 421,324, apparently is sufficiently different from the final test of the Enfield method that anticipation is not claimed. The author believes that both methods have their merits and that which should be selected would depend upon the length and specific volume of the pipe space and the economic justification for seeking a leak of particular size. Descriptions of the final step of the Enfield method, at least those presently available to the writer, are insufficiently specific to enable the author to estimate the number of manhours and volumes of gas that would be required to locate a leak. Sutton is probably right in claiming the Enfield method to be the better for locating a very small leak. The author believes, however, that for leaks having economic significance in pipe-contained cables the method described as step 3 in the paper requires less gas and less manhours, is more precise, and in addition is better adapted to gas pressure cables because nothing injurious to the cable is introduced.

In summary and conclusion, the author regrets that the prior work of which Sutton wrote was not acknowledged in the original paper. Interest in gas pressure cables is mounting rapidly in this country and all utilities who have such cables should be prepared to find leaks should any develop in their systems. Concerning sizes of leaks, The Detroit Edison Company not only has had no leak of 200 cubic feet per day (atmospheric pressure) in the pipe line under discussion but also, except for terminals, has had no leak at all. Nevertheless it is ready to locate leaks should any develop in that line, one under construction, or others contemplated. It is pleased to pass along significant information obtained in order that others may also be prepared, and welcomes any suggestions concerning means by which leak location may be improved. In this connection the author believes that the industry on this side of the ocean would welcome a published article from our friends on the other side giving some of the details of the equipment and techniques involved and experiences with them.

Bibliography of Relay Literature 1944–1946

AN AIEE COMMITTEE REPORT

THIS interim report was prepared by the project committee on relay bibliography of the AIEE relay committee in order to make immediately available this information to the entire industry. Previous to the last published bibliography on this subject, this material had been available only to the membership of the relay committee annually and to the industry at 10-year intervals. However, it was believed by the committee that the publication of such data should be made at 3-year intervals in order to place the information in the hands of all interested parties before such information should become obsolete.

The "Bibliography of Relay Literature, 1927–1939," and the "Bibliography of Relay Literature, 1940–1943," were published in pamphlet form and also in AIEE TRANSACTIONS, volume 60, 1941, pages 1435–47, and volume 63, 1944, October section, pages 705–09, respectively.

The articles listed in this bibliography (1944 to 1946) include all those appearing in the AIEE TRANSACTIONS or ELECTRICAL ENGINEERING as well as most of the chief technical publications of the world from January 1944 through December 1946. The listing of these articles is subdivided into several sections, depending upon the general substance of the article. In each section, the entries are numbered consecutively and listed alphabetically by years, the first significant word of the title determining the alphabetical position. Each article is listed in one section only.

Paper 48-6, recommended by the AIEE relay committee and approved by the AIEE technical program committee for presentation at the AIEE winter general meeting, Pittsburgh, Pa., January 26–30, 1948. Manuscript submitted October 30, 1947; made available for printing November 20, 1947.

This report of the AIEE relay committee, W. R. BROWNLEE, *chairman*, was prepared by a working group of the project committee. Personnel of the working group consists of C. E. PARKS, assistant system operating engineer, power department, Public Service Company of Indiana, Inc., Indianapolis, Ind.; S. GOLDSMITH, operating engineer, Cincinnati Gas and Electric Company, Cincinnati, Ohio; and C. L. SMITH, relay engineer, electric distribution department, Rochester Gas and Electric Corporation, Rochester, N. Y.

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No Discussion

Use of Reduced-Scale Models for the Solutions of Cable Temperature Problems—II

ANDREW GEMANT
NONMEMBER AIEE

JOSEPH STICHER
MEMBER AIEE

IN a previous paper[1] the authors showed that a reduced-scale cable installation can be used for the determination of cable temperatures. In that paper, first the theoretical basis of the scale reduction was given; next, certain tests were described to show that the method, as carried out in the laboratory, yields results that agree satisfactorily with results of a rigorous computation or with field data. Such a model installation may be looked upon as a novel laboratory tool, and the question arose in what practical cases this tool might be used to advantage. In the following pages a few tests are described to show some of these cases. The list is not meant to be exhaustive; it is to be hoped that, as the method becomes more familiar to other workers in this field, new uses will be considered and developed.

Check of Calculations

In the previous paper[1] it was pointed out that a rigorous calculation of temperatures of cables in ducts is not possible. There is, however, the possibility of carrying out calculations based on certain formulas and empirical constants. The basic idea of such a computation, which in the course of years has been improved in various ways, is described by Simmons[2] among others. The calculation is based on the thermal "Ohm's law," the total thermal resistance being divided into three lumped elements connected in series. The formula holds, strictly speaking, only for steady-state conditions, and transient cases are calculated by means of the so-called loss factor which, in its turn, is a function of the load factor. Conversion graphs for the evaluation of loss factors from load factors as well as other pertinent constants, including duct heating constants, have been established by the National Electric Light Association and the Insulated Power Cable Engineers Association.[3] It is worth noting that the cable temperatures calculated from formulas of this kind are usually quite close to reality; this is surprising in a sense if one considers that computation of transient conditions is based on the steady-state equation.

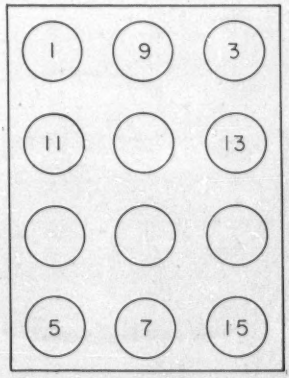

Figure 1. Diagram showing location of eight cables in duct

In practical instances it might be desirable for various reasons that a check of temperature data obtained from such formulas be made by means of another method. The model cable equipment is believed to be well suited for the purposes of such a check. Four tests of this kind are described in this section.

A standard three-by-four duct bank[1] was used in all tests covered by this paper. The bank contained eight 3-conductor 24-kv round shielded cables; the model cables (linear reduction factor equals 4.6) were furnished through the courtesy of the Okonite-Callender Cable Company. The location and designation of the eight cables are shown in Figure 1.

The reproduction in the model of moisture conditions in the field is of considerable importance. The moisture content of the soil as well as the wetness of the ducts usually varies along the length of a cable line. Concerning the former, it is perhaps best to use in the model a degree of moisture that corresponds to an average, representative moisture of the soil in which the field installation in question is located.* That was the procedure followed in our tests. With regard to the wetness of ducts, on the other hand, it is perhaps better to vary the conditions of ducts in the model. This may be done in a single test by keeping some arbitrarily selected ducts dry and others carrying the same load, wet; or else, in a duplicate run, keeping certain selected ducts dry in one run and wet in the other. A wet duct, as the term is used in this investigation on a model installation, is one containing 50 cubic centimeters of water; this amount ensures a conducting path between cable and duct.[1] It corresponds to 4 cubic inches per foot in the field. A dry duct contains no liquid water. In this manner both extreme conditions are covered and the cable operating personnel may select the values that, to their judgment, corre-

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ANDREW GEMANT and JOSEPH STICHER are both with the research department of The Detroit Edison Company, Detroit, Mich.

* The soil was a yellow clay, containing about 15 per cent water.

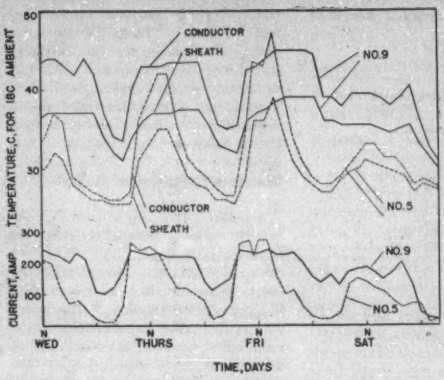

Figure 2. Loads and temperatures (conductor and sheath), obtained in the model for cables 5 and 9

Field schedule of August 1946

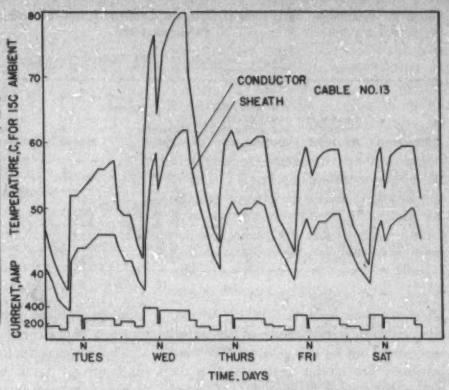

Figure 3. Loads and temperatures (conductor and sheath), obtained in the model for cable 13

Planned field schedule

spond to the actual conditions of the line, or, if they wish to be conservative, select the higher model temperatures as obtained in dry ducts. In the majority of tests to be presented this procedure was adopted. We shall come back to the question of dry versus wet ducts in more detail in a separate section of this paper.

A description follows of the four tests mentioned.

The first test was designed to reproduce conditions obtaining in a particular duct bank of The Detroit Edison Company for a period of one week in January 1946. The maximum temperatures obtained for a particular day are summarized in Table I. There are three sets of data: one based on measured sheath temperatures in the field, a second computed from loads, and a third from the model equipment.

The computations mentioned herein were carried out by the cable corrosion and loading division of our company; the authors are indebted to W. D. Sanderson for these data. The method of calculation employed is essentially that recommended by IPCEA[3] with the following revisions. The thermal resistance between the sheath and duct wall is determined by Kirke's[3] method. Variable or emergency loads are considered by applying an attainment factor or time constant according to Kirke and Walsh.[3] The thermal resistance between the duct wall and ambient sink is varied according to local conditions existing in the field; the respective constants were obtained by many years of field experience. The details of this computation, which involves many steps, and is based on standard methods, are not reproduced in this connection.

The linear reduction factor used in the model being 4.6, the time reduction factor is $4.6^2 = 21.2$, hence the week's schedule was reproduced in 8 hours; altogether, with 1 hour preheating period in 9 hours. The check of computed data by means of the model, excepting perhaps the cables carrying very low loads, is close.

The second test refers to a load schedule carried out in the field a week in August 1946. The loads in this test were somewhat higher than in the previous test. The temperature data obtained from the model for cables 5 and 9 are shown, together with the corresponding loads, in Figure 2. It may be seen that the temperature versus time curves are reproduced in all detail; the slight time lags between load and temperature curves are also visible. In this test two ducts, pertaining to cables 3 and 11, were dry, the other six wet. Maximum temperatures obtained for a particular day are summarized in Table II.

As may be seen, field data for the sheath are higher (on the average by 6 degrees centigrade) than model data obtained from wet ducts; and equal to or lower than model data from dry ducts. From this it follows that the condition of the ducts in the field was between dry and completely wet, as previously defined. Computed conductor-sheath differentials check fairly well with model data; computed data appear to lie on the safe side.

A third test of this type refers to a weekly load schedule not actually carried in the field, only contemplated. It involved drops in the load of half-hour duration at noontime, and the question was to what extent these drops would affect the

Table I. Maximum Temperatures, Degree Centigrade, for a Load Schedule Carried in January 1946

Ambient Temperature: 5 Degrees Centigrade—All Model Ducts in This Test Were Wet

		Sheath			Conductor		
Cable No.	Current, Amp (4 hr Average, Preceding Max Temperature)	Measured in Field	Computed From Load	Model	Computed from Sheath Temp, as Measured in Field, and Load	Computed from Load	Model
1	80	21.5		16.5	22.0		17.5
3	180	22.0	17.0	19.5	23.0	19.0	
5	205	23.0	22.5	20.0	25.0	25.0	25.0
7	55	21.0		15.0	21.0		15.0
9	220	27.0	25.5	24.0	29.5	28.5	29.0
11	220	26.0	25.5	27.0	28.5	28.5	32.0
13	220	27.0	25.5	24.0	29.5	28.5	30.0
15	220	25.0	25.5	21.0	27.5	28.5	27.0

Table II. Maximum Temperatures, Degree Centigrade, for a Load Schedule Carried in August 1946

Ambient Temperature: 18 Degrees Centigrade

No. of Cable	Condition of Duct in Model	Current, Amp (4 Hr Average, Preceding Max Temperature)	Sheath		Conductor-Sheath Differential	
			Measured in Field	Model	Computed from Load	Model
1	Wet	60	36.9	28.5	0.3	1.5
3	Dry	210	37.7	44.5	4.8	7.0
5	Wet	260	41.0	39.0	11.3	8.0
7	Wet	110	36.0	29.5	1.4	1.5
9	Wet	230	45.0	39.0	8.8	6.0
11	Dry	230	42.0	42.0	8.8	7.0
13	Wet	230	45.9	38.0	8.8	7.5
15	Wet	230	43.5	39.0	8.8	6.0

afternoon temperatures. While calculation according to the scheme as mentioned was carried out for this case, too, the temperatures are so far from steady-state values that a check by means of the model seemed advisable. In this test cables 3 and 11 were lying in dry ducts, the other six in wet ducts. Cables 9 to 15 carried the same, rather heavy load, the other cables carried lighter loads. Figure 3 shows load and temperatures for cable 13 (conductor and sheath); the influence of the half-hour load drops on afternoon temperatures is clearly visible. Computed and model data are summarized in Table III. It may be seen that computed data, particularly those for the conductor, agree closely with model data obtained from wet ducts.

A last test in this group refers to an arbitrary load schedule shown by the lower curve of Figure 4. All eight cables carried the same load, the load factor being 85 per cent. In this test all ducts were wet. From standard tables[2] the maximum load for round, shielded, 350,000-circular mil 24-kv cables, eight cables in duct bank, 85 per cent load factor, and 18 C, is 253 amperes. The question was: What permitted load can be derived from a corresponding model test?

The temperatures for cable 13 are shown in Figure 4. As can be seen, the load was made to decrease from day to day (1 day in the field = 68 minutes on the model) according to an arbitrary schedule. The maximum copper temperature for the second day is seen to be 85 C and for the third day 72 C. Taking 77 C as the allowable maximum temperature,[2] the maximum permissible load is determined by interpolation. The maximum load on the second day being 270 amperes, that on the third day 230 amperes, the allowable maximum is 246 amperes. This checks fairly well with the figure obtained from the standard table.

In this connection calculation of duct heating constants from model data and comparison with established values[2] might be of interest. A load schedule, to be discussed in the following sections, served as basis for the calculation, according to the formula

$$C_D = T_{dg}/8LH$$

(C_D = duct constant, T_{dg} = duct to ground temperature differential, L = 24 hour-loss factor, H = heat dissipated in watts per foot of cable). Table IV gives the data. The average for the duct constant for 8 equally loaded cables is 0.64, as compared with 0.77 as given by NELA.[2] A relatively low ground resistivity in the model, about 70 ohms, probably accounts for the difference.

Moisture Condition of Duct

A question that can be easily investigated in the model installation is the effect of the moisture condition of a duct upon cable temperatures. As mentioned in the foregoing section, ducts were kept either in a dry or a wet condition. The dryer a duct is, the greater is the relative role of radiation in the total heat transfer from the cable sheath to the duct wall. The model is strictly applicable for heat transfer by conduction.[1] While in a wet duct the error introduced by radiation is small; it is probably appreciable in a dry duct. It was advisable, therefore, to establish a correction formula which is valid for any degree of wetness. The application of this formula, the derivation of which follows, requires the knowledge of the duct wall temperature which can be measured in the model by means of a thermocouple, introduced into the duct or preferably built into the duct wall during construction. The correction formula to be derived is approximate in that it is strictly valid only for equilibrium conditions.

In the following, H denotes the heat generated per unit length and unit time, T_s sheath temperature, and T_d duct wall temperature as obtained in the model. We then have the following equation for

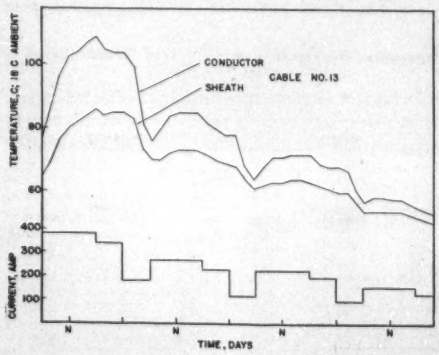

Figure 4. (left). Loads and temperatures (conductor and sheath) for cable 13

Daily load cycles of varying magnitude

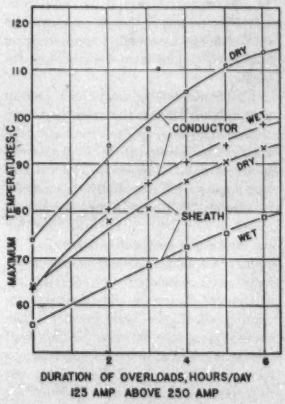

Figure 5. Temperature (conductor and sheath) for cable 3 for dry and wet duct conditions

equilibrium conditions between temperature and heat flow:

$$H=A(T_s-T_d)+B(T_s^4-T_d^4) \quad (1)$$

in which A is the thermal conductance between sheath and duct (varying with amount of water present) and B the radiation conductance, both per unit length. The constants A and B refer specifically to the model. B is known[4] to be

$$B=2\pi\sigma a/[1/\epsilon_d+a/a_d(1/\epsilon_s-1)] \quad (2)$$

with $\sigma=5.7\times10^{-8}$ abs units, a = radius of sheath, a_d = radius of duct, ϵ_d and ϵ_s = emissivities of duct and sheath (0.93 and 0.56, respectively). With $a=0.87$ centimeter and $a_d=1.11$ centimeter, B is 1.85×10^{-4} abs units, or 0.56×10^{-9} if H is measured in watts per foot.

We now introduce the scale-reduction factor q (4.6 in our installation) and write the equation, corresponding to equation 1, as valid for the field. H in the field is by disposition the same as in the model, and so is A by theory, if the degree of wetness is identical in the field and model installations, that is, the amount of water in the field is q^2 times that in the model. Furthermore, T_d in the field and in the model are identical because the heat resistances from duct wall to ambient are by theory the same, and the heat flow H is the same, as just mentioned. Instead of B, however, we have qB because of the factor a in equation 2, and for T_s we write T_x. Thus:

$$H=A(T_x-T_d)+qB(T_x^4-T_d^4) \quad (3)$$

If (T_s-T_d) is small compared with an average duct space temperature T_a, the last brackets in equations 1 and 3 may be simplified:

$$H=A(T_s-T_d)+4BT_a^3(T_s-T_d) \quad (4)$$

$$H=A(T_x-T_d)+4qBT_a^3(T_x-T_d) \quad (5)$$

in which two equations T_a is considered in first approximation as a constant, a representative value being 50°C = 323 K. Equations 4 and 5 represent two equations with two unknown quantities A and T_x, so that T_x, after A is eliminated, can be computed. If we denote T_s-T_d by T_{sd} and T_s-T_x by T_{cor}, then a simple calculation gives the correction T_{cor}:

$$T_{cor}=T_{sd}-H/[(H/T_{sd})+4(q-1)BT_a^3] \quad (6)$$

With $q=4.6$, $B=0.56\times10^{-9}$, and $T_a=323$, we have finally:

$$T_{cor}=T_{sd}-H/[(H/T_{sd})+0.27] \quad (7)$$

As an example, let us take data obtained at $H=11.8$ watts per foot. In a wet duct T_{sd} was 8 C; hence the correction term from equation 7 is 1.2 C. By this amount the sheath (and conductor) temperature in the field is less than in the model. As can be seen, this term is within the accuracy of the method. In a dry duct, on the other hand, T_{sd} was 22 C, hence T_{cor} from equation 7 is 7.4 C. For dry ducts the correction term is no longer negligible. Equation 7 was used in connection with all data in this investigation obtained from dry ducts, and the values as presented are all corrected data.

In order to show the difference in cable temperatures from dry and wet ducts, a particular load schedule, involving 125-ampere overloads of varying duration above a level of 250 amperes, was applied to the eight cables shown in Figure 1. In one test all ducts were wet; in another the duct containing cable 3 was kept dry, the others being wet.

Figure 5 shows the maximum temperatures versus duration of the overload for conductor and sheath of cable 3, both in wet and dry duct condition. The figure shows that the temperature difference between wet and dry ducts is about 10 C for a level of 75 C conductor temperature, increasing to about 15 C for a level of 90 C. Also, it can be seen that if a level of, say, 90 C is tolerated, then 125-ampere overload may be applied for nearly four hours with wet ducts, but only for about two hours with dry ducts.

Location of Cable in Duct Bank

It is known that cable temperatures depend on the location of the particular duct in the bank. The magnitude of this effect can be determined by the model. In connection with a load schedule as shown in Figure 4, maximum conductor temperatures versus location of the duct for various amperages were plotted on Figure 6. The diagram below the abscissa indicates the positions of the respective ducts. The temperature of cable 3 (dry duct) was not comparable with that of others and was not plotted. It may be seen that the center cable in the top row and the cables in the second row were the hottest. The corner cables, because of more favorable heat conduction into the ground, were the least hot. The maximum difference for a current of 265 amperes was 9 C.

Analysis of Temperature-Time Curves

Complete temperature versus time curves, as obtained from the model installation, permit immediate reading of maximum temperatures, as illustrated in the preceding sections. There are, however, two further kinds of information obtainable through an analysis of the curves, and these are dealt with in the following sections.

Relative Life of Cable Insulation

The following analysis is rather elementary and may be refined, if wanted, in several ways. The basic idea is that the cable insulation (paper and oil) slowly deteriorates chemically and when the amount of paper and oil that is chemically changed reaches a certain threshold, breakdown might take place, for instance, through thermal failure because of too high power factor. It is realized, of

Table III. Maximum Temperatures, Degree Centigrade, for a Contemplated Load Schedule

Assumed Ambient Temperature: 15 Degrees Centigrade

	Sheath			Conductor		
		Model			Model	
Time	Computed	Wet Duct, Cable No. 13	Dry Duct, Cable No. 11	Computed	Wet Duct, Cable No. 13	Dry Duct, Cable No. 11
Wed AM	50.3	58.5	69.0	77.9	76.5	87.0
Wed PM	58.0	62.0	74.0	82.0	80.0	94.0
Sat PM	47.5	50.0	55.5	60.0	59.5	66.5

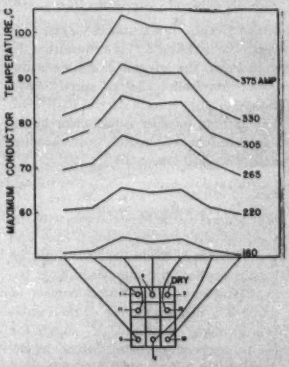

Figure 6. Variation of conductor temperature with location of duct in a bank for various loads

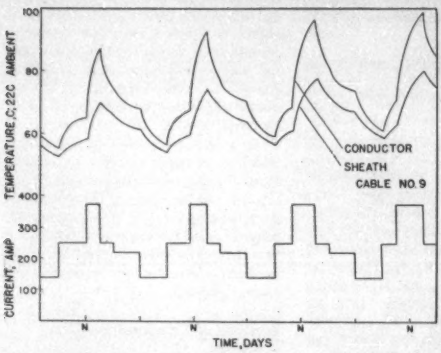

Figure 7. Loads and temperatures (conductor and sheath) for cable 9

Daily overloads of varying duration of 125 above 250 amperes

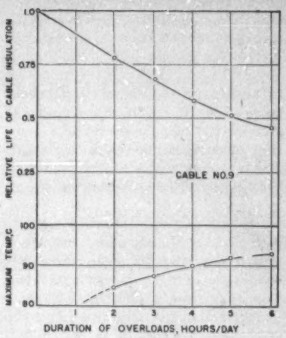

Figure 8. Relative insulation life and maximum temperatures vs. duration of daily overload for cable 9

course, that under present loading conditions the life of the cable itself depends on many other factors, chiefly related to the sheath and partly of an accidental nature, and not so much on the chemical deterioration of the paper and oil. It is emphasized, therefore, that the following consideration does not refer to the life of the cable as a whole, merely to the probable life of the cable insulation. If, however, temperature limits are raised appreciably, the importance of chemical changes as a life-determining factor as compared with the other factors is increased.

The rate of chemical deterioration (oxidation, polymerization) may be generally expressed as me^{bT} with m and b constants and T = temperature. Such a law is assumed to hold in this case also. The total chemical change in one day is then given by:

$$m\int_0^{t_d} e^{bT}dt = mM \tag{8}$$

where t = time and t_d stands for one day. Since the function $T(t)$ is known from the model test, the integral M can be evaluated numerically. Let us specify equation 8 as written for a normal daily load. Let us now consider a particular higher load schedule and write the corresponding daily chemical change as

$$m\int^{t_d} e^{bT'}dt = mM' \tag{9}$$

the primed quantities referring to the specified over-load schedule; they too are known from the model test. If higher load is applied on a number of days, being a fraction f of the total number of days; if t_i' denotes the life of the insulation in days; if finally M_i stands for the total chemical change before breakdown takes place, then the following relation holds:

$$M_i = ft_i'mM' + (1-f)t_i'mM \tag{10}$$

the first term standing for the total change during high load days, the second for that during normal load days. From equation 10 the life is given as

$$t_i' = (M_i/m)/[fM' + (1-f)M] \tag{11}$$

The life could only be evaluated in absolute figures if the constant M_i/m were known, which is not the case. A relative figure for the life may, however, be calculated in the following manner. Let us write an equation corresponding to equation 11 for $f=0$, that is for a cable that does not carry high loads. If the life of such a normal cable is denoted by t_i, then we have

$$t_i = M_i/mM \tag{12}$$

Dividing equation 11 by 12, we obtain finally

$$t_i'/t_i = M/[fM' + (1-f)M] \tag{13}$$

in which only known data occur. Equation 13 gives the relative insulation life as a fraction of the life of a normal cable.

In the following evaluations $b = 0.0693$ was used because, with this value, an increase of the temperature by 10 C doubles the rate of the chemical deterioration. This is in keeping with many known chemical reactions,[8] although in other cases the rate is trebled for 10 C rise in temperature. Any other value, if preferred, may be substituted for the one assumed here.

In practice, overloads in the narrower sense of the word, are applied only a few days per year; in this case f is small, and equation 13 yields a relatively little decrease of insulation life. For the following calculation, however, we consider daily loads that are higher than the accepted standard. In this case f becomes unity and equation 13 takes the form

$$t_i'/t_i = M/M' \tag{14}$$

A load schedule, as mentioned previously with reference to the moisture condition of ducts, was analyzed according to equation 14. The data, obtained by means of the model setup, upon which the analysis was based is shown in Figure 7 specifically for cable 9. The computation was based on conductor temperatures. The integral M for the normal cable in each case was computed from an extension of the portion of the curve prior to the sudden rise due to the high load. This normal curve occurring with loads of 250 amperes is, of course, under equilibrium conditions the same every day; in the model test there is a slight increase of the general level because equilibrium has not been reached. The result of the analysis is shown in Figure 8. At the lower part are shown maximum temperatures obtained versus the duration of the high loads; at the top part is shown the relative life t_i'/t_i of the insulation, the life for normal load being unity. This additional information is of more practical value than the mere knowledge of peak temperatures because it also contains the time element during which the peaks prevailed. If, for instance, a probable reduction in life of about 20 per cent is tolerated, an overload of 125 amperes above 250 amperes may be applied for about two hours every day (under the condition of eight equally loaded cables in a bank).

Rate of Rise of Cable Temperature

A second kind of information that may be evaluated from the graphs obtained is the maximum rate of temperature rise. This rate might have significance in certain cases because it determines the rate of oil expansion which in its turn should not exceed certain limits. To be specific, let us consider a cable run, one section of which is located lower than the rest of the

Table IV. Computation of Duct Constant from Model Data

T_{dg}	L	H	C_D
29.0	0.77	7.7	0.61
33.5	0.39	17.3	0.62
35.0	0.41	17.3	0.62
40.0	0.43	17.3	0.67
42.0	0.46	17.3	0.66
44.0	0.48	17.3	0.66

cable. In the course of time, oil might gradually drain from the higher to the lower section, causing the latter to be fully impregnated. If the temperature rises and the oil has no time to flow from the fully impregnated low section into the neighboring ones, the lead sheath in the lower section expands; if the rise of temperature is too rapid, it might be damaged. It is obvious that more detailed information of this mechanism will be necessary before full benefit from a knowledge of the rate of temperature rise is derived.

As an illustration of such an analysis, a test was carried out in which a 3-hour overload of various magnitudes above 240 amperes was applied. The temperature versus time curve is not given for this case. The analysis of these graphs resulted in the curves in Figure 9. The lower part gives maximum temperatures and the upper part relative insulation life versus overload currents. The center part shows maximum rates of temperature rise per hour (figures valid for the field installation). As mentioned before, the maximum permissible limit of rate can only be calculated if the specific conditions for each case, such as the contour of the cable run and the plastic characteristics and tensile strength of the sheath, are known.

Scale Reduction in the General Case

In both the previous and present investigation only one cable or one duct bank was dealt with. As a result, the problem was treated as a 2-dimensional case, the third dimension, parallel to the cable, having no influence on the calculations. There are, however, practical problems for which this condition does not hold. Such a problem would be, for instance, the influence upon the cable temperature of a steam pipe running close to a cable duct at an angle with the latter. This problem is a three-dimensional one, and it is worth investigating briefly whether or not the scale reduction scheme can be used in this general case as well as in the special 2-dimensional case. This question is answered in the present section. No actual tests along these lines were carried out.

In order to answer this question, let us examine the equation[6] giving the temperatures at any point in space and any time if a spherical heat source generates instantaneously the amount of heat H_o. For our particular purpose, it is advantageous to replace H_o by Ht_o where H is the heat generated in unit time and t_o is a small time interval. The equation then reads:

$$T=\frac{Ht_o}{8\pi^{3/2}D^{1/2}\rho c r a t^{1/2}}\left[\exp\left(-\frac{(r-a)^2}{4Dt}\right)-\exp\left(-\frac{(r+a)^2}{4Dt}\right)\right] \quad (15)$$

where D = diffusivity, ρ = density, and c = specific heat of heat conducting medium, a = radius of spherical heat source, r = radius vector of any point in space, t = time.

What are the transformation equations if the form of equation 15 should remain unchanged? Material constants remain unchanged and the temperature T should remain the same. From the expressions in the bracket, it can be seen that we have, as in the 2-dimensional case.

$$r=qr_r \quad (16a)$$

$$a=qa_r \quad (16b)$$

$$t=q^2t_r \quad (16c)$$

Introducing these substitutions into equation 15 and dividing numerator and denominator by q^2, q is left over in the latter. Thus, H/q has to be identified with H_r, or:

$$H=qH_r \quad (16d)$$

If, therefore, a 3-dimensional installation is reduced by a linear reduction factor g, the total heat rate must be reduced in the same ratio.

This is the same result that was previously established for a cable, only in a more general form. A cable in a 3-dimensional sense has a definite length, and in the model this length is reduced by a factor q. Since the heat rate for the whole length is reduced in the same ratio, the heat rate per unit length of cable is the same, and in this form the result was formulated previously.

It follows that, by using equations 16a to 16d, any 3-dimensional heat conduction problem can be solved by means of a reduced scale model. If, for instance, a steam pipe crosses a duct run, both the diameter of the steam pipe and its distance from the duct is reduced in a ratio q. The length of the cable must be sufficiently long for the disturbance caused by the pipe not to extend to the ends of the cable. The temperature of the pipe is maintained at the same level in

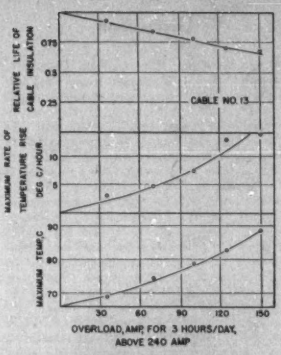

Figure 9. Relative insulation life, maximum rate of temperature rise, and maximum temperature versus three hours daily overload for cable 13

the model as in the field. Transient heat flow from such pipes into the ground was treated in a separate paper[7] recently published.

In closing, it is pointed out that the present investigation, together with the previous one, completes to a certain degree the authors' work on this method. A possible continuation of the work depends partly on whether there is sufficient interest among cable users in applying the method to pertinent questions in this field. A possible application, one in which some interest has been expressed, is a determination of duct and soil resistances, sometimes referred to as duct-heating constants, for varying moisture content of the soil. Since the soil in the model can be irrigated artificially, these varying conditions could be maintained relatively easily in a reduced-scale model installation.

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Discussion

R. J. Wiseman (The Okonite Company, Passaic, N. J.): I am glad to see that Gemant and Sticher have written another paper on the studies that they have made on reduced-scale model cable installations. In this paper they report on a comparison of field duct installations with the model set-up for ducts and show that fair agreement is obtainable in the model set-up. As field conditions are quite variable along a duct route, we cannot expect close agreement between laboratory tests and field tests, rather the laboratory tests can be considered as indicative of the magnitude of the temperatures to be expected. Also, our knowledge of the thermal constants for the various parts of the thermal circuits are none too good, so we cannot expect too close agreement of computed and test results.

I like very much the reporting of results with wet ducts showing how helpful it is to have water in a duct, as it seems to act as if the water short circuits the convection and radiation factors in the zone between the cable and the duct wall, thereby improving the conduction of heat to the duct tank.

As the authors state on page 3 of the paper, the model gives a means for determining duct heating constants, and I hope that it will be possible for a program to be set up to carry out a complete study of these constants. There are some of us who feel that a clearer understanding of the constants that we now use should be obtained, particularly when the thermal resistance of cables in ducts is compared to buried cables. The model setup that the authors use has a soil surrounding the duct bank having a thermal resistivity of 70 thermal ohms per centimeter.[8]

They report in Table IV of an average duct constant of 0.64 for eight cables in the duct bank as compared to the NELA value of 0.77. I believe we should take the constant for a 10-way duct bank, which is 0.74, instead of an 8-way duct bank, as the model set-up has the equivalent surface for radiation of a 10-way duct bank. Although the agreement between laboratory test and assumed constant is only fair, it does indicate that for the latter the earth thermal resistivity is low.

Figure 6 gives a good picture of how beneficial it is to have the cables in outside ducts only and that corner ducts are preferred locations.

I hope no one will get the impression from Figure 7 that daily overloads should be carried on cables with the reduction in cable life, as indicated in Figure 8. If load cycles as shown in Figure 7 exist, then the load factor should be found and the cable designed in accordance with this load factor, and allowable operating temperature. Overloads are supposed to occur only occasionally and usually for not over 300 hours per year.

I believe the authors have produced a method for the study of temperatures on cables in duct banks which can go a long way to obtaining some of the information that is needed both for transient and steady-state loading of cables. I hope that they will continue this work and in time make further reports to the industry.

Andrew Gemant and **Joseph Sticher:** It was gratifying to hear Wiseman's favorable comments on our paper. We also think that the suggested method might prove useful in solving some existing uncertainties concerning the loading of high-voltage cables. Whether we shall be able to continue this work depends on various circumstances, not all under our control.

Wiseman's remark concerning the frequency of overloads is correct. Daily high loads were used in the test only to illustrate the results of the proposed calculation of insulation life in a conspicuous manner.

Finally, it may be worth mentioning that the model method is applicable not only to buried cables, or cables in ducts, but also to extra-high-voltage cables in pipes. In case the pressure medium is oil, the application is quite straightforward. In case the medium is gas, the correction term for radiation, as explained in the paper with reference to ducts, must be applied, and the results thus obtained will then be close to expected field data.

An Electroacoustical System

ERIC A. WALKER
MEMBER AIEE

THIS PAPER describes the application of certain electrical and acoustical phenomena to the solution of a specific problem, namely, the finding of torpedoes which have been lost in the course of proof testing. The equipment explained consists of a mechanism for generating a supersonic signal which travels through the water or sediment in all directions. This signal is detected by a directive-listening hydrophone which can locate the approximate bearing of the source in both azimuth and depth. The latter equipment is portable and can be mounted on small locating vessels. By a proper search procedure, the torpedo can be fixed within a few yards probable error, from where it can be retrieved by a diver.

Although this application is a very specific one, the problem itself is much more general and has wider application. The principles of underwater signaling which are described have been developed largely within the past few years and have further applications in underwater detection, underwater signaling, geophysical prospecting, and in material testing.

The Specific Problem

Before torpedoes are accepted for use by the Armed Forces, each one must make a successful test run, in which it must run a course within specified limits of depth, direction, and duration. Such a test, therefore, demands that each torpedo must be risked in an actual run in the ocean or other suitable body of water. In these proof runs the warhead, which contains a detonator and explosive, is replaced by a water-filled dummy head. Usually a fully-armed torpedo ready for a war shot is not buoyant, and its total weight exceeds its displacement by some 300 or 400 pounds. To insure that the torpedo will float at the end of a trial run, advantage is taken of the lack of explosive, and the dummy warhead is filled with water or a salt solution of a predetermined specific gravity. This liquid is blown out of the warhead by air through a self-closing valve at the end of a predetermined run or when the engines stop. Displacing the water by air then renders the torpedo buoyant, and at the end of its test run, it should float on the surface, from where it can be sighted visually and retrieved. Unfortunately, the margin of safety afforded by this system is not large because of space limitations. Too frequently because of a failure of some part of the mechanism which results in incomplete head blowing, or because of other failures that prevent complete burning of the fuel, the torpedo does not become buoyant and therefore sinks to the bottom. It is unfortunate that those torpedoes which are erratic and get off course, sometimes ending miles from where they should be, show the greatest propensity to fail to come to the surface. It is also unfortunate that proof-testing stations are not located on clear water so that one can see the bottom and recover the torpedoes by simple visual search.

The specific problem, then, is to find some mechanism by which a torpedo can make its location known when it is on the bottom under several fathoms of water or even when it is stuck in the mud at the bottom of a test range. In more general terms, however, the problem is to locate any object which is operating in a medium where the usual signaling methods, such as visual or electromagnetic, fail.

Possible Systems of Detection

In this particular problem it is possible to provide the object which might later have to be located with some device which will indicate where it is. This is a sensible thing to do, since the percentage of those torpedoes which become lost is rather high, and there is no doubt that one will go to considerable expense to find it once it is lost. The system evolved is not applicable to finding those devices in which no such provision has been made. For instance, this system cannot be used to locate a sunken submarine or the wreck of an airplane which is on the bottom of the ocean unless sufficient foresight has been exercised to provide the projector part of the locating system in the lost object. This is now standard practice with submarines but obviously not with airplanes.

Various indicating systems were considered, among them those which might cause the emission or ejection of bubbles, heat, a dye, chemicals, or even a marker buoy. Such devices will be considered very briefly in order, enumerating their advantages and disadvantages and showing why all were discarded.

First, the ejection of a stream of bubbles. It is quite easy to provide a torpedo with a source of compressed air which can be released slowly, giving a train of bubbles which will be visible when they break on the surface of the water. In fact, a small compressed air supply at the 3,000 pounds per square inch pressure available in many torpedoes provides a bubble train which will last for many hours. The difficulties encountered in seeing bubbles, however, are quite obvious. Unless the bubbles are large, they cannot be seen for any great distance even with smooth seas, and if the sea happens to be disturbed or even mildly turbulent, they cannot be detected at all from a distance of more than a few feet. Another consideration is the drift of the surrounding water caused by river currents or by tides. Bubbles sufficiently large to be easily visible (about 1 inch diameter) rise with quite limited velocity, namely,

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ERIC A. WALKER is head of the department of electrical engineering, Pennsylvania State College, State College, Pa.

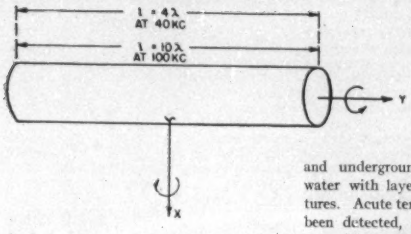


Figure 1. A simplified line source

about two feet per second. Therefore, in a tide they do not come to the surface over the spot where they are released. For instance, bubbles released from a point 100 feet below the surface emerge 500 feet downstream in a 6-knot current. This large error greatly reduces the usefulness of such a system, especially when as in this case, the location must be accurate within a few feet. Perhaps the greatest disadvantage of the bubble system is caused by so many of the torpedoes sinking or driving themselves several feet into the mud on the bottom. This mud has sufficient weight and viscosity to prevent the ejection of the stream of bubbles, which of course renders such a system entirely useless.

The second method might be to generate heat in the body of the torpedo and detect the torpedo's presence by the rise in ambient temperature. At first glance this method seems quite promising, since temperature differences of the order of a fraction of a degree are quite easily recorded. The scheme, however, has all the disadvantages of a bubble system, resulting from the drift of the water or the loss of the body in mud, plus one other quite serious limitation. The ocean or even a small fraction of it cannot be considered as an isothermal medium, since the sun, the tides, and the flow of rivers and underground streams stratify the water with layers at different temperatures. Acute temperature gradients have been detected, and variations of 10 or more degrees within a few fathoms are not at all uncommon. In fact, the change in density at such layers makes it possible for submarines to sit stationary "on a thermocline".

A buoy scheme might be considered. A possible solution is to have the torpedo eject a light balsa wood buoy when it exceeds a certain depth or when its velocity falls below a certain predetermined minimum. The latter can be accomplished by a flap valve held in place by a trigger on which the water exerts a force while the torpedo is in motion. When the valve opens, the buoy is ejected, trailing a line attached to the sunken torpedo. The disadvantages to such a scheme are immediately apparent. First, a considerable length of line must be stowed within the body in such a way that it can unroll quite freely and with little restraining force. Again the effects of tides and currents are quite evident, since the buoy will trail far downstream unless its lift is considerable. The trailing or horizontal force is a direct function of the length of line, and tests show that the size of buoy which insures a rise to the surface in any appreciable current is excessive. The most serious objection to such a device, however, is that if the torpedo is immersed in mud, the release mechanism will not operate, and the buoy cannot rise to the surface.

Another solution which momentarily gave promise was the ejection of a dye similar to that used and so widely acclaimed for the saving of shipwrecked airmen when stranded in small boats. This dye, called fluorescein, gives a widespread and brilliant yellow mark if it is released on the surface of clear, nonturbid ocean water. Unfortunately, its effect is almost nil if the water is at all dirty, and because its specific gravity is about equal to that of water, it does not rise to the surface if released at any appreciable depth. Such a method proved to be quite unfruitful.

Other methods, such as changing the chemical content of the water, were suggested. Since it is possible to detect very small changes in oxygen content by methods which have been developed in physiological research, such a scheme was investigated. However, there appear to be random variations in the oxygen content of sea water, and such investigations were not pursued very diligently.

A much more fruitful method than merely putting out a marker device appears to be the generation of some phenomenon which can be propagated or transmitted through the mud and water. In considering such methods, one immediately thinks of light waves, infrared radiation, electromagnetic waves, and sound waves.

If a light source is placed in the lost body, one can expect the intensity to decrease as the inverse square law, at least as a first approximation, if one neglects such factors as reflection and absorption. Unfortunately, in practice absorption cannot be neglected. Measurements show that the absorption of the total visible spectrum is approximately 6 decibels every 10 meters even in the clearest sea water. In other words, one half of the energy of a plain wave is lost every 10

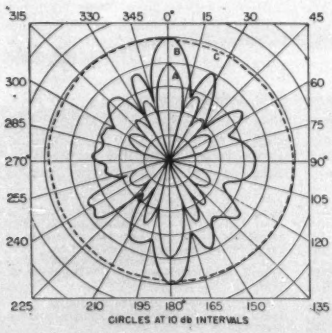


Figure 2 (left). The patterns at 40 kc

A. Theoretical pattern for a line source four wave lengths long

B. Recorded pattern in the *X* plane

C. Recorded pattern in the *Y* plane

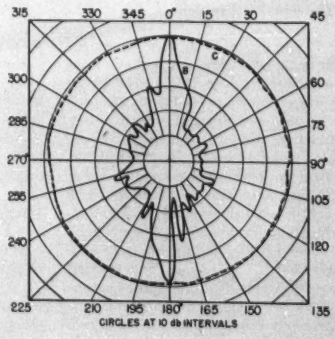


Figure 3 (right). The recorded patterns at 100 kc

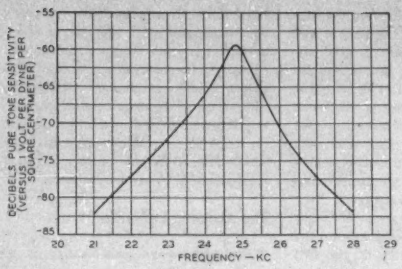


Figure 4. The response - frequency curve of a typical transducer

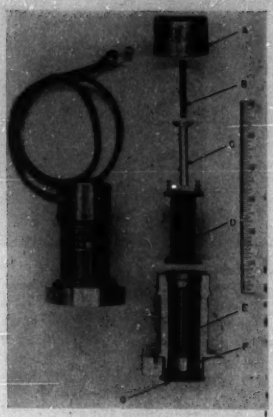


Figure 5. A simple, rugged magnetostriction projector ready for assembly

The biasing magnet, the nickel tube, and the rubber hose used to connect the terminating disk to the water or to the torpedo shell. At these frequencies rubber is a good conductor of compressible waves

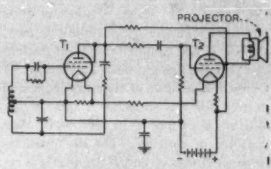


Figure 6. The oscillator used to drive the acoustic projector

meters.[1] This loss is increased by turbidity, bubbles, foam or jetsam. Of course, it would not be necessary to use light in the visible spectrum, since detectors other than the eye can be used. Although the entire light spectrum has not yet been explored, there is little reason to believe that there are preferred frequencies which are not absorbed at this rate, although there is some slight evidence to show that some frequencies in the green-yellow spectrum are absorbed less than others. In summary, to the extent of present knowledge, the absorption of light waves, including the infrared band, is too great to make their use practical.

Electromagnetic waves in the radio-frequency spectrum might be used, and indeed submarines have been known to receive radio signals when their antennas are submerged. Here again the absorption of such waves is tremendous, increasing very rapidly with increase in frequency. The loss is about 3.4 decibels per meter at 10 kc and about 34 decibels per meter at 1 megacycle. Although one avoids attenuation by using the longer wave lengths, a consideration of the problem shows that the transmitting and receiving equipment become excessively large at these frequencies. Moreover, the size of antennas necessary to permit direction finding increases very rapidly as the frequency is lowered. Another phenomenon which has not been sufficiently explored is the attenuation of electromagnetic waves when they pass from sea water into air or vice versa. We only know that the loss for these conditions is very high. Little work was done with this medium.

The use of sound waves offers more promise than any of the former schemes. Sound travels in sea water with a speed of approximately 5,000 feet per second,[3] so that no particular difficulty is caused by the delay between the transmission and reception of sound signal. However, sound energy, like that of light, is subject to absorption when passing through sea water, but fortunately in this case the amount is not prohibitive. Some measurements show that the absorption of 28 kc sound is about 0.003 decibel per meter, while at 90 kc it is about 10 times this amount. In other words, the energy of 28-kc sound drops to about one half in traveling through 1,000 yards.[2] Again although there is something to be gained in going to the longer wave lengths, it is found that the transmitting apparatus becomes more unwieldy at the lower frequencies, and the receivers must be large in order to get proper directivity. There are several other advantages which favor the use of underwater sound. First, sound waves not only travel through water but will penetrate mud. Second, it is possible to build efficient supersonic speakers and receivers of reasonable size. Third, the method does not require very large amounts of acoustic power.

There are a number of disadvantages, but none of them is important enough to make the method unusable. The most prominent among the disadvantages is the refraction of the sound caused by temperature gradients in the water or changes in salinity.[4] This limits the range and makes the apparent direction in depth somewhat erroneous. Another is the background noise which is present in the water and which has many causes—waves, rain on the water surface, the noise caused by the machinery of boats and the cavitation of their propellers, and lastly the noises caused by shrimp, some kinds of fish, and other marine life.

The Design of an Underwater Sound System

The choice of the frequency for which the system should be designed is influenced by several factors. As pointed out previously, the attenuation of sound waves approximately doubles each time the frequency is doubled. However, one must have an appreciable directivity to the listening hydrophone in order to tell the direction from which the sound is coming.* For a given directivity, one approximately doubles the size of the hydrophone as the frequency of the system is halved. Therefore, the choice of a working frequency partially becomes a compromise between these two factors. This point can be illustrated in another manner. Consider Figures 1, 2, and 3. Figure 1 shows a sketch of a "line source" type of transducer—a simple metallic tube which can be induced to vibrate radially. When this device is used as a projector and is immersed in water, a

*The terms "transducer," "hydrophone," and "projector" are sometimes used quite loosely. The following definitions will be used here: The transducer is any device which transforms one kind of energy into another, that is, electrical to acoustic energy or vice versa. A hydrophone is a device whose principal use is to convert sound waves in water into an electric signal. A projector is a device whose principal use is to convert electric energy into acoustic energy in the water.

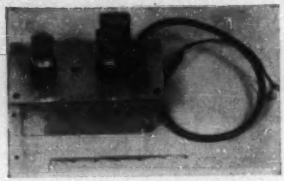


Figure 7. The finished oscillator ready for installation

polar graph of the sound intensity can be plotted around any axis at any chosen frequency. Since a transducer is a bilateral-energy converter, the same graph governs its performance as a projector or as a hydrophone. Figure 2 shows the patterns at 40 kc for a line source 10 inches long having a diameter of about 1 inch. In this diagram curve A is a plot of the theoretical sound intensity around the X axis. Note that this curve is shown on a logarithmic or decibel scale with the intensity in the strongest direction taken as the base or reference intensity. The most prominent feature of this curve is its pronounced lobe in the zero direction and the three secondary lobes to each side of the major lobe. Such a curve is quite familiar to the acoustic or radio engineers, since it is the familiar radiation pattern of a straight line source or an antenna.[5, 6] Curve B is a curve of the recorded-sound pattern for this transducer. The major lobe has been moved 10 decibels for convenience in plotting. The discrepancies between curves A and B are partly a result of the construction of the transducer and partly a result of errors in determining the curve. Any practical transducer must have an electric winding and a protective covering which tend to produce discontinuities which show as overemphasized lobes or shadows. There is always difficulty in recording the pattern of a transducer, since one cannot find a homogeneous medium in which to make the measurement. Reflections from the edge of the water and from the surface and bottom are always present to give partially erroneous readings. Curve B is also shown on a decibel scale. It is seen that the sound emitted along a 36 degree radial line is 20 decibels less than that along the reference axis. Conversely, the response to a plain-pressure wave oriented at 36 degrees gives 20 decibels less voltage or one per cent of the power given by a like wave at 0 degree. It might also be noted that the major lobe on the 0 axis is 20 degrees wide at the half voltage points, that is, the points 6 decibels down from the maximum.

The curve marked C in Figure 2 is a graph of the sound pressure around the Y axis. For this orientation the projector acts like a point source, and the field has a circular symmetry.

Figure 3 shows clearly the effect of a higher frequency or shorter wave length. At 100 kc the major lobe should be much more narrow than at 40 kc, and there should be nine secondary lobes in each quadrant. Because these lobes are so narrow, the theoretical pattern has not been plotted. However, it can be seen that at 100 kc the major lobe in the pattern around the X axis is only 8 degrees wide at the 6-decibel points, and many of the minor lobes have almost disappeared. On the other hand, the pattern around the Y axis is almost unchanged.

It is fairly obvious that any electromechanical system which is expected to work in resonance, as this is, will show a familiar resonance type curve as one plots some property, such as the magnitude of the vibration, as a function of frequency. In order to produce highest efficiency, it is desirable to operate more nearly at resonance. Figure 4 shows a resonance curve for the transducer.

Fortunately, one is not limited to the frequency range easily heard by the ear, since one can employ beat frequency techniques and shift the chosen transmitting frequency to an audible one. Actually, a frequency of approximately 25 kc per second was chosen. This sound, which is in the supersonic range, was converted to an 800-cycle note for listening at the receiving end. In passing, it might be noted that at 25 kc the wave length is approximately $2^1/_2$ inches in water.

The Acoustic Radiator

The next design consideration is to choose an efficient acoustic radiator. Obviously, a knocker or doorbell might be used, but the amount of sound emitted by such a device in the usable region is small, and the efficiency is very low. Within the past few years, considerable work has been done in the theory and design of both magnetostriction and crystal transducers. In this case either type might have been used, but a magnetostriction one was chosen because of its simplicity and ruggedness. In essence, the projector consists of a nickel tube mounted on a small metallic diaphragm. The nickel tube is given a magnetic bias by inserting an "alnico" magnet through its center. Vibration is produced by an alternating-magnetic field induced by a current in a coil which surrounds the tube. This arrangement is such that vibration takes place in the longitudinal direction rather than radially. By choosing a length of tube which is mechanically resonant at the proper frequency, high efficiency of electric-to-acoustic power conversion can be obtained. The purpose of the diaphragm is to match the mechanical impedance of the tube to that of the surrounding water in which one wishes to produce the supersonic-sound field.

Figure 5 shows a cut-away view of a magnetostriction projector. A is a metal cap. B is a thin-cylindrical "alnico"

Figure 8. A self-contained unit consisting of battery, oscillator, and projector. This type is for airplane launching

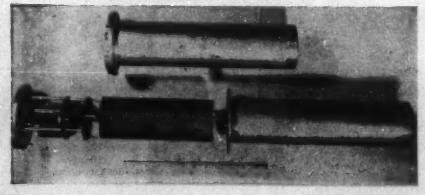
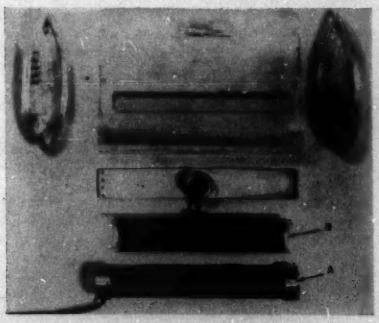


Figure 9. An exploded view of the receiver hydrophone



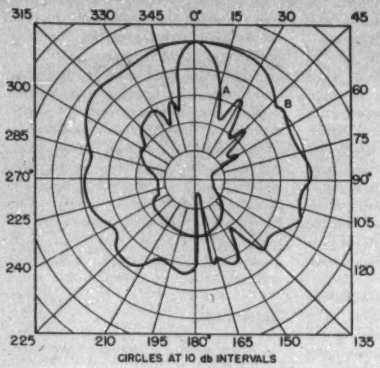


Figure 10. The receiver pattern in the azimuth plane (A) and vertical plane (B)

Figure 12. The completed receiver

magnet used to provide the proper magnetic bias in the nickel tube. *C* is an eddy current shield which surrounds the magnet. *D* is the winding, and *E* shows the nickel tube itself. Note that the nickel tube is slotted to reduce eddy currents. It is mounted on a small brass boss *F* which is connected to a rubber diaphragm *G*. This projector, in a favorable mounting, will produce a reasonably spherical acoustic field and at a distance of one meter will produce a field strength of about 30 decibels above one dyne per square centimeter. This represents an approximate energy conversion of about 10 per cent. Actually, the sound so produced is well above background noise at a distance of 1,000 yards.

A simple and very rugged electrical oscillator is necessary to provide the driving power for the electro-acoustic transducer. Many of the torpedoes to be tested contained a battery which was used for main engine propulsion. Since it was possible to design a circuit to work off the main battery voltage, this provided an almost unlimited source of power for the transmitter. The particular system chosen used two 3Q5-GT tubes and consisted of a frequency-stabilized oscillator driving a single power stage. The circuit is shown in Figure 6, while Figure 7 shows the assembled oscillator ready to mount.

Some versions of the device were constructed for use in torpedoes which operated with turbines and therefore had no storage battery. In these cases the whole assembly, consisting of projector, oscillator, and battery power, was assembled in a water-tight can which could be removed in one piece. One type of this assembly is shown in Figure 8. This particular version is also designed to be dropped from aircraft at rather high speeds.

The Acoustic Receiver and Circuits

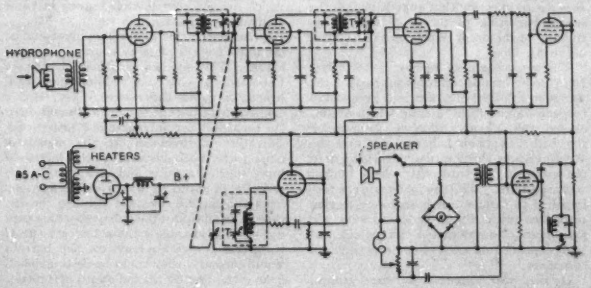
The acoustic receiver is not quite as simple as the transmitter. In the first place, because it is designed to pick up small amounts of energy, it must have high sensitivity. Moreover, in order to permit some degree of direction finding, a certain amount of directivity is required in the listening hydrophone. The particular version finally adopted consists of one or more tubes of nickel surrounded by an electrical winding in which the signal is generated. Because it is necessary to attach this receiver to a small retriever vessel and to listen while the boat is underway, the assembly is mounted in a streamlined housing. This posed another problem, namely, the finding of a material which could be machined easily and which offered approximately the same acoustic impedance as sea water. Fortunately, lucite meets both requirements.

After some experience with this system, it was found desirable to provide some degree of search in depth. In the construction of the hydrophone, it was found necessary to place an acoustic baffle approximately one-half wave length behind the vibrating element in order to increase the front-to-back discrimination, or the ability to hear sounds ahead of the hydrophone better than those behind. The idea of making this baffle in such a manner that it could be rotated in order to throw the listening beam up or down was tested and found to work. This, then, provided an easy method of tracking the source of sound as the receiving vessel passed overhead.

Figure 9 shows an exploded view of the finished receiving hydrophone. *A* is the actual acoustic vibrator, and *B* is the rotatable baffle. The shell is formed completely from lucite.

Figure 10 shows the patterns of this hydrophone in both vertical and horizontal planes. It is seen that in neither plane is the major lobe very sharp. This again is a compromise, since although a sharp lobe assists in obtaining bearing accuracy and improves the signal-to-noise ratio, one might pass over a noise source if searching rapidly with an extremely sharp-lobed transducer. The sensitivity of this particular receiver is approximately 100 microvolts per bar in a 1,000-cycle band centered at 25 kc.

Figure 11. The receiver circuit



The complete receiving circuit is shown in Figure 11. This is a rather conventional circuit in that it consists of two stages of tuned radio frequency amplification and a beat frequency oscillator, reducing the 25 kc note to 800 cycles, at which point there is further amplification before the output stage. The output indication consists of a speaker or headphones in parallel with a meter for visual indication. A complete assembly ready for shipboard installation is shown in Figure 12.

Search Procedure and Use

At a later date a number of refinements were added, such as making the receiving head trainable from the wheelhouse of the receiving vessel. However, in most cases the coxswain found it only necessary to don earphones and to steer the vessel in the direction of the loudest sound. The method of search is rather simple. The boat is merely steered in the direction of the loudest sound, and an attempt is made to train on the beam in a vertical as well as horizontal plane. When the ship passes over the source of sound, there is a rather noticeable and abrupt drop in sound intensity. At this point a buoy is launched, and another search is made from a different tack. If the two or three locating buoys are relatively close together a diver can be sent down to locate and retrieve the lost torpedo. In some installations it was found desirable to equip the diver with a portable receiving hydrophone which he could carry on his chest. In most cases the signal from this hydrophone was monitored on the deck of the retrieving vessel, and the diver was given verbal instructions concerning which direction he should face and how far he should go.

That the device and system were successful is easily shown by facts. The Navy has written that this system at one installation accounted for the recovery of over 250 electric torpedoes and over 175 steam torpedoes. The same system with diver's hydrophone accounted for 60 additional electric torpedoes and about 100 steam torpedoes, these recoveries representing a saving of over $14,000,000. However, these figures should not lead one to suppose that this is the only use of such a system. This is but one very specific application which the need made it necessary to develop. Many other uses for sound waves are being found in the realm of testing metals for flaws, underwater signaling, the plotting of geophysical profiles, and even in the design of time delay lines for radar trainers.

Obviously such a development as that described in this paper is the result of the work of a number of men. This system was developed by a team of scientists working at the Harvard Underwater Sound Laboratory during the war years. Among those who made major contributions are Dr. Harvey Brooks, Dr. K. C. Morrical, R. R. Thompson, L. N. Miller, and others.

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Discussion

T. A. Daly (Westinghouse Electric Corporation, Sharon, Pa.): I should like to compliment the author on his very comprehensive discussion of the problem of locating lost torpedoes. The possible systems outlined are very reminiscent of the early days of electric torpedo development at the Westinghouse Ordnance Division, as several of these methods were used at our Lake Pymatuning range before supersonic locators became available. One method not mentioned is the Navy device known as a torchpot, consisting of a canister containing calcium phosphide which, upon contact with water, gives off a stream of gas bubbles. These bubbles ignite upon reaching the surface, resulting in a momentary flare and a puff of white smoke.

The acoustic locator which we used most was a self-powered unit developed by the Naval Ordnance Laboratory and produced a 45-kc signal of very good directivity. It had, I believe, an additional advantage in that the signal was pulsed, making it considerably easier to distinguish it from other noises in the area. Our search procedure with this unit was slightly different, in that the diver's headphones were also connected to the receiver and he was able to guide himself to the torpedo, instead of following instructions from above. During more than four years of experimental firing of nine different types of torpedoes at Lake Pymatuning, not a single unit was permanently lost.

I might mention that considerable testing is done in depths of water at which divers cannot operate, and under these conditions, of course, locators of any type are of no value. For this reason, the tendency has been to run the torpedoes whenever possible in a buoyant condition and to eliminate head-blowing equipment. However, mechanical proving runs will continue to be made in shallow waters, and here such locators are of definite value.

R. B. Vaile, Jr. (Armour Research Foundation, Illinois Institute of Technology, Chicago, Ill.): Before commenting specifically on the very interesting paper which was so ably presented by Walker, may I first express my wholehearted agreement with his question regarding the logic of the Navy's decision to proof test every torpedo. It is my recollection that approximately the same proportion of torpedoes fail on rerunning of the proof test as on the first run of the proof test.

The importance of retrieving torpedoes from the bottom became obvious early in the war to most of the organizations involved in torpedo development, among which are included the Naval Ordnance Laboratory the Bell Telephone Laboratories, and the Westinghouse Corporation, in addition to the group at Harvard with which Walker was associated. All these groups were first impelled to develop methods for retrieving torpedoes primarily in order to save the man-hours of development time which had been expended on each of the experimental torpedoes.

It was also apparent to these groups, after they had been in contact with the situation for a short time, that there was a problem of at least equal importance in the recovery of production torpedoes in order to save the production man-hours which had entered into their construction. For these reasons, each of the organizations mentioned ultimately developed and used one or more special devices for aid in the recovery of submarine torpedoes. Sonic generators covering much of the range between 10 and 50 kc were used. While the Harvard group used magnetostriction transducers, I believe all the others used piezoelectric transducers.

The sonic equipment developed by the Naval Ordnance Laboratory made use of a frequency of approximately 45 kc and was distinguished from all the others in being of the pulsing rather than the continuous type. The pulsing technique had the advantages which are, of course, well known in their equivalent application in radar as follows: First, a much larger power per pulse could be obtained from an energy source of limited extent. This enabled the Naval Ordnance Laboratory device to be self-contained and, hence, to avoid the necessity for electric connections from the outside to the inside of the torpedo. Second, there is a rather considerable improvement in the signal-to-noise ratio, both because of the increased power during a pulse and also because the problem of standing waves and reflections from the surface does not confuse the listener. In addition, the pulse repetition rate was made adjustable in these devices so that, if several torpedoes were on the bottom simultaneously, they could be distinguished from each other by the different pulse rates.

As soon as such aids to the location of torpedoes, as have been described by Walker and the others mentioned here, were in widespread use, another bottleneck in the recovery of torpedoes appeared. This was the difficulty of obtaining sufficient divers to complete the recovery after the location of the torpedo had been determined. For this reason the buoy method mentioned by Walker was developed to a considerable extent by the Naval Ordnance Laboratory. This development took the form of a buoy which was impelled to leave the torpedo by a small powder charge which was sufficient to expel the buoy through several feet of mud. As a further part of this equipment, when the buoy was picked up by the retriever launch, a small steel cable was lowered using the buoy line as a messenger and was engaged by an automatic latch attached to the torpedo. The torpedo was then raised to the surface by this small steel cable and towed to the dock where it taken out of the water.

Eric A. Walker: The discussions by Daly and Vaile both bring out the fact that equipments similar in nature were constructed by the Westinghouse Company and by the Naval Ordnance Laboratory. As a matter of fact, the development of the equipment described and the one at the Naval Ordnance Laboratory proceeded in parallel lines of attack and with some exchange of information. The fact that one group arrived at the conclusion that "pinging" was best, and the other that a steady-state signal was best, is a natural consequence of separate developments. There are arguments in favor of each system. As pointed out in one of the discussions, a pinging system allows one to differentiate more easily against background noise. It also allows one to put more energy into the water if the limitation in energy arises from either the heating of the elements or lack of an adequate battery supply. This is not strictly true if the limitation is caused by the cavitation of the water on the face of the transmitting hydrophone. However, it has been shown that for very short pulse lengths, one can attain greater pressures without cavitation than with longer pulses. In neither of the equipments described was cavitation a limitation.

There is one advantage in favor of the steady signal which cannot be overlooked and that occurs when the receiver has a relatively narrow beam pattern. If one wishes to scan rather rapidly throughout the entire 360 degrees in azimuth, one can be more sure the sound source will be heard if the sound source is a steady one. If it is a pinging device, one may sweep so rapidly that the lobe of the receiving hydrophone passes by the transmitter while it is not transmitting and so miss the signal. These are minor points and do not affect the usefulness of either system in any serious manner.

It is pointed out by Daly that this type of locating device is useless if the water is so deep that the divers cannot retrieve. This is absolutely true and many attempts have been made and are still being made to devise retrievers which are automatic in operation and which will do something to return the torpedo to the surface. Undoubtedly, one or more of these plans will be advanced to a successful conclusion.

The argument between piezoelectric and crystal transducers is an old one. Actually, there is not much to choose between them. If a transducer can be made for this type of use, using the piezoelectric principle, then it can be made using the magnetostrictive principle. This would not be true if extremely high frequencies were used. There is little argument as to which system and which type of transducer is the most rugged since both of them are quite adequate for this purpose. As a matter of fact, there is little difference in cost, and in cases like this cost is not of any great consequence since the equipment to be protected is many times more valuable than the protecting device itself.

FCC Diathermy Design for Low Harmonic Radiation and Good Frequency Stability

E. W. CHAPIN
NONMEMBER AIEE

W. K. ROBERTS
NONMEMBER AIEE

M. C. MOBLEY
NONMEMBER AIEE

FOR SOME YEARS the number of diathermy machines employed for therapeutic heating has been increasing, and at the same time the radio frequencies employed by these machines have gradually become of greater and greater importance in the communications industry. During and after the recent war the use of high frequency energy for industrial heating processes reached unprecedented proportions. These factors, operating in the same direction, have given acceleration to the development of critical interference problems. Besides the problems arising from the fundamental frequencies employed by diathermy and industrial heating equipments, the radiation of harmonic emissions has become of great importance because of the use of the frequencies from 54 megacycles and up for emergency, frequency modulation broadcast, and television services, in which diathermy and industrial apparatus and broadcast and other receiving apparatus will be operated in close proximity, as well as the danger of interference to radio aids to navigation.

Extended consideration was given the problem, and after hearings had been conducted, Part 18 of the Rules and Regulations Relating to Industrial, Scientific and Medical Service was finally adopted by the Federal Communications Commission in June 1947. These rules were intended to prevent interference in the majority of situations, with full realization that in a number of cases interference still will be experienced. Additional corrective measures will be required in these particular cases. The technical rules finally adopted represented a compromise between the requirements of the communications services and users of high frequency heating devices. The problem of interference originating from the operation of industrial heating and diathermy equipment received international consideration at the International Radio Conference held recently in Atlantic City and resulted in an international allocation of frequencies for the operation of such devices.

At the hearings preceding the adoption of the rules by the Commission, certain

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E. W. Chapin, W. K. Roberts and M. C. Mobley are chief, assistant chief, and engineer, respectively, of the laboratory division, Federal Communications Commission, Laurel, Md.

The project was carried on under the direction of G. E. Sterling, chief engineer, Federal Communications Commission. Valuable assistance was received from George Ikelman, John E. Knight and the entire laboratory division staff.

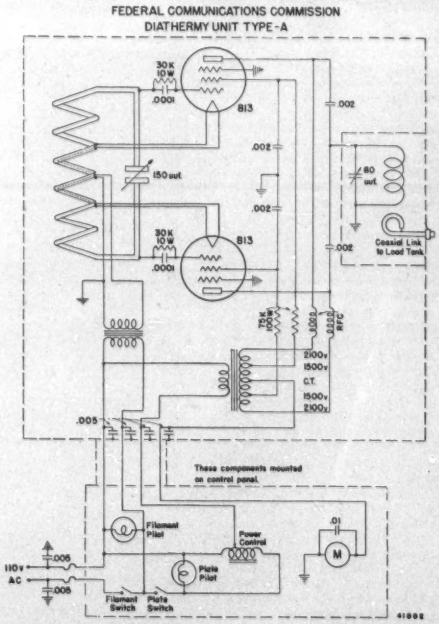

Figure 1. Circuit diagram of the diathermy unit

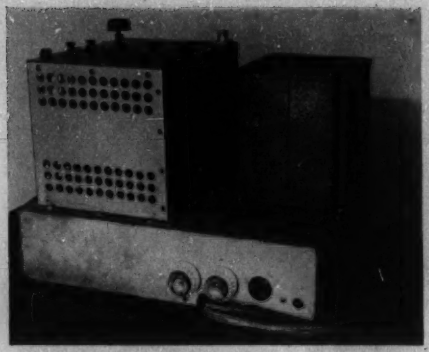

Figure 2. The oscillator unit

The oscillator tubes are in the front half of the upper compartment, and the plate tank circuit is in the rear half. The plate tank fine-tuning adjustment knob and the radio-frequency output connector are at the top

manufacturers raised a question as to the practicability of economically satisfying the proposed requirements. When this question arose the Commission's laboratory at Laurel, Md., was instructed by the chief engineer to undertake a project to develop a self-excited diathermy machine of adequate therapeutic value, meeting the requirements of the Commission's rules covering such apparatus, for the 27.320 megacycle band. The idea was simply to show that the proposed rules could reasonably be satisfied if certain precautions were taken in design and construction and not to develop the best possible equipment or to develop a prototype which would be followed by the industry. The following remarks mention diathermy specifically, but the principles outlined are also valid for other types of high frequency devices.

Many diathermy machines which have been submitted to the Federal Communications Commission Laboratory for tests in regard to compliance with FCC rules have failed to pass such tests. The most frequent shortcomings are

1. Inadequate frequency stability under load variations.

2. Excessive radiation of harmonics of the fundamental frequency.

Deficient frequency stability in most cases was due to use of an unstabilized power oscillator directly coupled to the load. This type of circuit for generating radio frequency power dates back at least to 1916, and has generally been abandoned as obsolete for ordinary communication purposes for a number of years.

Harmonic radiation from these machines is of two types. (1.) That due to radiation from the generator as the result of insufficient shielding; and, (2.) that due to radiation from the load circuit and applicators. The former may obviously be mitigated by shielding and the latter may be reduced to the necessary extent by the design of the coupling circuit to discriminate against the harmonics.

It should be emphasized that the design of the subject diathermy unit was completed and the final model was produced several months before any commercial models were submitted which met the new requirements. Hence, it should not be construed that the design is based on information gleaned from examination of them. Furthermore, as will be made evident in succeeding paragraphs, there is nothing new or radically different in this design that was not known and practiced by many engineers in other fields of the electronic and communication art for a number of years. The Laboratory staff merely applied these standard engineering methods to the particular case of diathermy apparatus design.

Design Principles

Frequency Stabilization

There are several alternatives which provide frequency stability independent of load variations, such as crystal control and a master oscillator power amplifier. In the present case, where economy of tubes and component parts is desired, an electron-coupled power oscillator was used. See Figure 1 for the circuit diagram. In a pentode oscillator of this type the control grid circuit is isolated from the plate circuit by the electrostatic shielding effect of the screen and suppressor, each of which is at radio-frequency ground potential. In our case the grid and plate circuits are each tuned to 27.32 megacycles. The grid circuit could be tuned to 13.66 megacycles, further reducing the effect of load variation on frequency, at some sacrifice in output power. It will be noted that at radio frequency the plates of the two oscillator tubes are operated in parallel, while the grids are connected in push–pull. Hence, even if there is plate-to-control-grid feedback, such feedback in the active tube is practically neutralized by the corresponding feedback in the inactive tube. In the oscillator described the maximum frequency variation over the entire range of applied load encountered in diathermy service was 12 kilocycles at 27.32 megacycles. This compares with some self-excited diathermy units which showed a frequency variation of as high as 2,000 or 3,000 kilocycles at 27.32 megacycles.

Figure 3. Schematic diagram of the output tank circuit

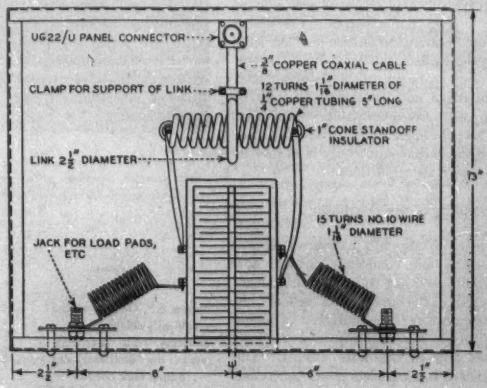

Figure 4. The output tank circuit

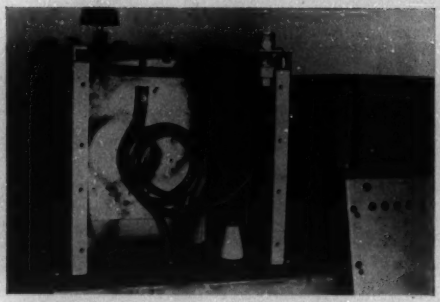

Figure 6. The plate tank circuit, opened for inspection

The capacitance is that obtained between the inner and outer boxes. The tuning adjustment is by means of a metal vane moved by the knob

The frequency drift due to warm-up and voltage variations could be compensated but for the sake of economy and simplicity no such measures were taken. The width of the assigned band easily accommodates the 56-kilocycle drift measured over a full load run of six hours. The frequency variation with voltage results in combined amplitude and frequency modulation, but the sideband energy is confined to the assigned band.

Power Supply

Reference to Figure 1 shows that the oscillator is connected in the conventional two-tube self-rectified circuit in which only one tube is operating on each half cycle of the applied voltage. This arrangement is simple and economical, doing away with rectifiers filters, and the necessity for a time delay between the application of filament power and load. The disadvantages are the reduced output per tube and increased peak voltage for a given power output. The design could be altered to include rectifiers feeding the oscillator unfiltered direct current, thereby reducing these disadvantages. The rough note is tolerable of itself so long as the frequency stability is adequate to keep the operation in the assigned band and the harmonic intensity is in compliance with the regulations.

Shielding of the Oscillator

Refer to Figure 2. Here we see the oscillator unit completely enclosed in a metallic enclosure which is entirely independent of the outside metal cabinet. In this way there is the equivalent of double shielding. No radio-frequency tank currents flow in the outer cabinet. The oscillator is a complete power-generating unit, fed from the 115-volt 60-cycle power line, delivering its radio-frequency output to a coaxial cable connector. The control cable is fed through a shield braid. It was desirable to shield the tubes with a perforated metal cover.

Output Tank Circuit

In diathermy work it is essential that the power be delivered to a wide range of loads with several types of applicators. Generally speaking, the applicators fall into two broad classes:

1. Inductive, including cable, drum, disk, and hinged drum. This category generally comprises a conductor about 10-feet long, coiled into the configurations mentioned. The over-all length is such that the applicator is resonant at roughly the operating frequency.

2. Capacitive, including pads, cuffs, and air-space. These applicators are in a class which is used to treat the patient with electric fields. The length of the leads modifies the impedance presented by the capacitive plates to the output circuit.

All of these applicators are normally used with wide variations of spacing and configuration, in order to apply the energy to the desired part of the body in the intended manner, and with these variations there goes the large range of resistance and reactance into which the diathermy must feed power.

A number of load circuit variations have been used in the diathermy art. The one shown in Figure 3 and Figure 4 was adopted for the present design because the shunt capacity to ground offers a further reduction in harmonic radiation. All applicators commonly employed in diathermy treatments may be used with the circuit, all connecting to the same pair of terminals.

Coupling Circuit

The load circuit is link-coupled to the oscillator for three reasons:

1. The load circuit may be physically separated from the oscillator, as in our design.

2. The oscillator and load tank circuits may be well shielded from each other to make the desired mode of coupling the only important one.

3. Such an arrangement can employ two link coils, one at each tank, each of which can be shielded against capacitive coupling, thereby doubling the effectiveness of the electric shield.

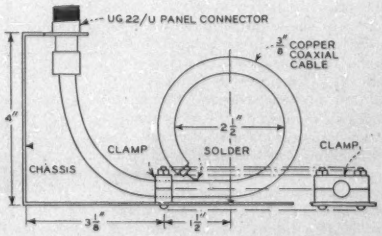

Figure 5. Details of the electrically-shielded link coupling coils used between the oscillator and output tank circuits

Figure 5 is illustrative of the type of shielded-link coupling coil which is used. This link will be recognized as the same as the shielded-loop antenna used for low-frequency direction finders and field-intensity meters. Other forms of shielding, such as the Faraday screen, are effective provided the impedance to ground from all points on the shield is low at the highest frequency harmonic which is to be considered. Considerable harmonic energy exists up to at least 400 megacycles in an ordinary 27.32-megacycle diathermy.

Oscillator Tank Circuit

Efficient operation of class *C* oscillators and amplifiers requires the plate current to consist of short-duration pulses which occur at the minima of the plate voltage wave. The plate-current pulses are very rich in harmonics, becoming more so with the shorter pulses that accompany high efficiency operation. The object of our tank circuit design is to allow reasonably efficient operation and at the same time provide freedom from radiated harmonic energy, either from coupling into the load circuit or from direct radiation from the tank circuit.

Figure 6 shows the tank circuit construction. The capacitance is obtained between the inner box and the outer shield. In this way the plates of the tubes are connected to ground by a high capacitance, actually about 80 micromicrofarads, and the capacitor itself has no leads. The result is that the harmonic components of the plate-current pulses flow to ground through the capacitor, never reaching the inductance, which has a much higher impedance. If the capacitor had leads of appreciable length there would be, in effect, an inductance in series with the circuit from plate-to-ground and the higher harmonic frequency components of the plate-current pulses would divide, some passing to ground through the circuit described, and some through the tank coil. If a significant fraction does pass through the tank coil, energy will be transferred by the link circuit to the applicators in spite of the shielded link coil, since the shielding is not effective against inductive coupling. The applicators serve as an antenna and excessive harmonic radiation is produced.

Therefore, the basic requirements for low harmonic radiation are

1. A low impedance path from plate-to-ground, achieved through the use of a high capacity, which has the important characteristic of negligible lead length and consequently low-lead impedance.

2. Confinement of the output coupling means, so that coupling is inductive only, and that such inductive coupling is made only to the tank coil.

With respect to part 1 it might have been possible to employ a conventional variable air capacitor or a vacuum capacitor. However, to obtain the benefits desired it will be necessary to employ them in such a way that the lead inductance is very small, as outlined before.

Figure 9. The diathermy, ready for operation, with air-space treatment arms connected

Cable and pad electrodes on the floor

Figure 8. The complete diathermy installation in its cabinet, showing the relation between the components

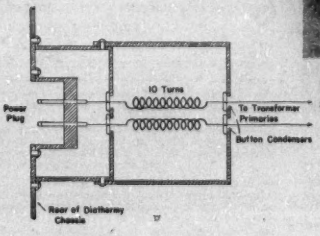

Figure 7. A proposed power-line filter for the prevention of harmonic coupling to the power line

Power Line Filter

Design of a low-radiation diathermy unit requires that the power line leading to the unit be well isolated from harmonic fields. If it is not, it will be found that the diathermy machine and power line will operate as a large antenna system and spoil the remainder of the design.

In the unit described the radio frequency tank circuit is completely enclosed in a compartment separated from the power cord entrance, so simple mica by-pass capacitors were found adequate. To be effective it is necessary that the capacitor show a low capacitive reactance at frequencies as high as 400 megacycles. A slight amount of inductance in series, such as from an inch or two of leads, will almost void their effectiveness at the highest harmonic frequencies.

Button-mica or lead-through-type capacitor would be ideal for this service because their leads are negligible in length. Sufficient capacity must be used to by-pass the low frequencies. Figure 7 shows a proposed power line filter design for diathermy which should be highly effective. Here each line wire is led to a pi-filter section. It is important to shield the entire filter because if it were mounted in the open in a diathermy cabinet there would be coupling from the intense harmonic fields within the oscillator to the series choke coils.

Performance

The characteristics of the complete diathermy described are as follows:

1. Frequency variation with all possible manipulation of external loads and controls ±12 kilocycles.

2. Frequency variation when operated continuously into a fixed full-load—56 kilocycles. (Frequency variation allowed by type approval section of FCC Rules—±112 kilocycles from 27.320 kilocycles.)

3. Harmonic field intensity—maximum of 5 microvolts per meter at 1,000 feet. (Harmonic field intensity allowed by type approval section of FCC Rules—25 microvolts per meter at 1,000 feet.)

4. Therapeutic effectiveness—tested by Council on Physical Medicine, American Medical Association, through co-operation of Howard A. Carter, Secretary of the Council, and found to satisfy its requirements.

5. Power output—approximately 150 watts. The output fluctuates above and below this value, depending on the particular load arrangement.

Summary

This paper has set forth the principles which pertain to the elimination of frequency instability and harmonic radiation from diathermy equipment, illustrating the application of the principles with an actual model. In this way it is hoped that emphasis will have been placed on the manner of obtaining the results as well as on the principles. The problem of high frequency harmonics requires facility with high frequency techniques.

Discussion

E. L. White (Federal Communications Commission, Washington, D. C.): This paper affords a convenient opportunity to point out the seriousness of the consequences of unnecessary radiations.

At 3:15 p.m., Eastern Standard Time, on November 26, 1947, the supervisor of Station WSY, one of the stations serving international aircraft flying the North Atlantic, reported serious interference on all of the 6 megacycle frequencies used for air-ground communications. This same interference was observed and reported by a similar station, VRT in Bermuda. This report was referred to the Federal Communications Commission for investigation, and the source was found on December 1 by means of a radio-direction finder network to be in the vicinity of Johnstown, Pa. A mobile direction-finding unit was dispatched from the Philadelphia Office of the Federal Communications Commission to Johnstown and on December 3 located the interference to be originating in an industrial heater located in a chair manufacturing plant. The management of the chair factory immediately notified the manufacturer of the industrial heater, who in turn dispatched a representative to take necessary corrective action. Additional tests made on December 19 indicated that the interference had been eliminated.

A multitude of services occupy the radio frequency spectrum and actively use frequencies as high as 10,000 megacycles. Equipment is being designed and placed in service which will occupy frequencies up to 30,000 megacycles. Many of these services are intimately connected with safety. One of many good examples is "GCA" (ground controlled approach), a device using radar principles and procedures to guide aircraft to runways under conditions where the pilot cannot see the ground or his surroundings. It is easy to see the danger to life and property should interference destroy the utility of such a system at a crucial moment.

The Federal Communications Commission's rules provide certain frequencies which may be used for industrial, scientific and medical purposes without the necessity for shielding. These provisions were adopted in realization that for certain applications shielding was impractical. If other frequencies are to be used, public safety demands that the manufacturer of the equipment, as well as the user, take any necessary steps to insure that any radiation which may inadvertently result during the use of the equipment is contained within the premises on which the equipment is located.

D-C Amplifier Stabilized for Zero and Gain

A. J. WILLIAMS, JR.
MEMBER AIEE

R. E. TARPLEY
MEMBER AIEE

W. R. CLARK
MEMBER AIEE

Synopsis: An electronic amplifier for d-c measurements is described in which the zero is stabilized by the use of a contact type of modulator vibrating at 60 cycles per second. The gain is stabilized, by over-all d-c feedback, to a small fraction of one per cent. Circuits and precautions are described which have been used to adapt this type of amplifier to the measurement of contact potential drop, thermocouple electromotive force for infrared gas analysis, electromotive force of glass electrodes for pH determination, and current from capacitors resulting from leakage and absorbed charge. The relation between noise from thermal agitation and the system response is discussed.

The Problem

WHEN MAKING measurements of small direct voltages or currents, the need has been felt for a d-c amplifier, to place ahead of the indicating or recording instrument in order to measure still smaller voltages or currents.

The problem is to retain speed and stability as the gain of the amplifier is increased and the input power level is reduced.

The stability problem divides itself into gain stability and zero stability (zero output for zero input).

Retaining stability is a problem because of fluctuations of power line voltage, mechanical disturbances (shock, vibration, and tilting), magnetic fields, thermal gradients, and electric disturbances of all kinds.

Block Diagram

Figure 1 is a block diagram of the system which has been developed to meet the problem.

The conversion from direct to alternating current avoids the conventional d-c amplifier and its zero drift.[1,2,3]

The synchronous reconversion to direct current permits the use of over-all d-c feedback to stabilize the gain. By proper attention to the transmission versus frequency characteristic of the feed-back loop, notably in the output filter, in accordance with the teachings of Nyquist[4] and Bode,[5] the amplification of the a-c amplifier can be increased, without causing oscillation, to the point where the steady-state instrument reading is dependent almost entirely upon the direct current to be measured (and the value of the attenuator) and only to a negligible extent upon other factors.

The low-pass filter on the input attenuates frequencies above the response band of the system and so avoids needless overloading of the amplifier.

Circuit Variations and Applications

In adapting this type of amplifier to various applications, the circuits must be varied for

1. Voltage measurement or current measurement.
2. Low impedance or high impedance.

Figure 2*A* shows in simplified block form the series type of feedback used for voltage measurement. Because of the high amplification of the a-c amplifier, the drop across the input converter is reduced to a small fraction of the drop across the resistor *f*, as discussed later under the heading Gain Stability. This small drop means only a small current through the input converter, that is, small compared to the current through the instrument, and small enough to maintain essentially an open-circuit condition across the circuit being measured (null potential measurement).

Figure 2*B* shows in simplified block form the parallel type of feedback used for current measurement. Because of the high amplification of the a-c amplifier, the current into the input converter is reduced to a small fraction of the current through the resistor *R*. This small current means only a small drop across the input converter, that is, small compared to the drop across *R* (so that *R* and *f* form an Ayrton shunt on the instrument current), and small enough to maintain essentially a short circuit condition across the circuit being measured (null current measurement).

The terms "open-circuit condition" and "short-circuit condition" are only relative compared to the circuits being measured. Since the measured circuits vary in impedance from one application to another, ranging from something less than an ohm up to more than a million megohms, it is helpful to use more than one type of input converter circuit.

Figure 3 shows a converter circuit for medium and low impedance including an input transformer with a voltage stepup to the amplifier. One useful transformer has a voltage stepup of ten to one from each half of the primary, and has a secondary impedance of 0.6 megohm (hence a primary impedance of about 6,000 ohms) at 60 cycles, the converter frequency. Measurements check the d-c impedance looking into the converter circuit as 6,000 ohms. Figure 3 also shows details of the output-converter circuit, an output filter, the series type of feedback suitable for voltage measurement, and an input filter which does not shunt any direct current and hence is suitable for voltage measurement.

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A. J. Williams, Jr., R. E. Tarpley, and W. R. Clark are all connected with the research department of the Leeds and Northrup Company, Philadelphia. Mr. Williams is chief of the electrical division and Mr. Tarpley and Doctor Clark are supervisory research technologists.

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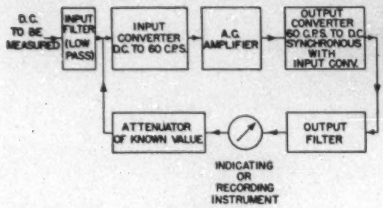


Figure 1. Block diagram of d-c amplifier and measuring instrument, showing the feed-back loop

The converters permit the use of the a-c amplifier

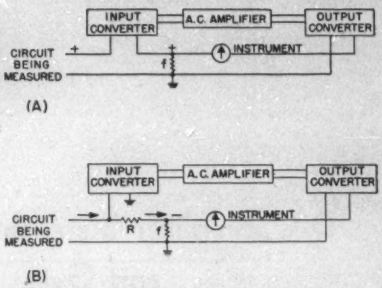


Figure 2. *A* shows arrangement for voltage measurement using series type of feedback *B* shows arrangement for current measurement using parallel type of feedback

Figure 4 shows a converter circuit with *RC* (resistance-capacitance) coupling[6] to the amplifier. Although this arrangement provides no voltage stepup to the amplifier, it does provide the highest input impedance. With certain precautions it can be held above 100 megohms. Figure 4 also shows details of the output-converter circuit, an output filter, the parallel type of feedback suitable for current measurement, and an input filter suitable for current measurement.[6] Inspection will show that the input filter C_1 R_1 C_2 does attenuate higher frequencies which might otherwise find their way from the input to the amplifier and cause needless overloading. Since the current into the converter circuit is negligible, the d-c drop across $R_1 + R_2$ in series with the converter circuit is negligible; hence negligible drop is inserted into the measured circuit, as is desired for current measurement.

One of the early applications of the amplifier was to an urgent wartime need[7] to measure a d-c contact drop of less than 1/2 millivolt. Available deflection millivoltmeters were too critical concerning the constancy of the resistance of the leads and their connections, to give the accuracy and the fast critically damped response that was needed. The several amplifiers that were built and used on this job followed the arrangement of Figure 3 except that the input-converter circuit was as shown in Figure 4, since the range, 1/2 millivolt, was large enough not to require the step-up transformer. The input filter used two 5,000-ohm resistors, and two 1.7-microfarad capacitors. The response time of these early amplifiers was about two seconds. The stability of zero and gain was good to one division on the built-in deflection-type indicating meter. One division corresponded to ten microvolts on the input, and this stability was adequate for the application.

Another application for the amplifier, which also started from a wartime need,[8] was the measurement of the electromotive-force output of a set of differential thermocouple junctions used for infrared gas analysis (range 50 microvolts). For this job the voltage step-up transformer was used, therefore the circuit was just as shown in Figure 3. Since the thermocouples were of low impedance (50 ohms or less), the impedance of the input filter was made as low as capacitor limitations would permit. It consisted of three 2,000-ohm resistors and three eight-microfarad paper-capacitor sections which made up a unit of convenient size. This filter provides an attenuation for 60 cycles of more than 300 to 1 which experience proved to be adequate. The response time was about two seconds, and was independent of external-circuit resistance, at least up to 600 ohms. The zero was good to 0.5 microvolt without any adjustment, and fluctuations over limited periods of time were less, as will be discussed later.

Several amplifiers electrically similar to those previously mentioned are being used in the Leeds and Northrup factory for production testing. Here they are often applied as null detectors only, and for this service a varistor is switched around the deflection meter to give a semilogarithmic response and so keep the pointer on-scale and active for unbalances up to about 2,000 times the minimum detectable unbalance.

Figure 5 shows an experimental instrument, built in response to another wartime need, which has since been used to measure the minute currents from paper capacitors, and so evaluate their fitness for use in certain sensitive instruments.[6,9] The circuit is arranged for current measurement essentially as shown in Figure 4. C_1 and C_2 are polystyrene capacitors (one microfarad) to minimize spurious currents. R, R_a, R_1 and R_2 have the values 0.25, 0.25, 0.05, and 0.05 megohm, but only the value of R enters into the measurement. Since it is only 0.25 megohm, it can be made up of manganin wire to well within ± 0.1 per cent without fear of change with temperature, humidity, or time. In spite of this relatively low value of calibrated resistor, the fundamental range of the instrument is $\pm 5 \times 10^{-10}$ amperes. This is derived from a full scale voltage change across resistor f (2.5 ohm) of only 250 microvolts. Such a low voltage change is permissible because of the small zero voltage offset of the contact type of modulator.[10,11] Experience on this job indicated a value less than five microvolts. The capacitor-type modulator, as recently reported,[12,13] may have a zero voltage as great as 20 millivolts, or 20,000 microvolts; hence larger voltage changes are generally used, and the precise manganin-wire resistor must be replaced by the less precise higher valued film or composition resistors even for ranges considerably larger than the $\pm 5 \times 10^{-10}$ amperes. Figure 5 shows a dial at the left for changing to larger current ranges. Originally, this was accomplished by making the R_a resistor Figure 4 in the form of an Ayrton shunt, and by changing the position of the upper input lead on this resistor. Subsequently, it has been done by changing the position of the same lead on the R resistor, thus reducing the active part of R. The latter method has the advantage of returning the upper input lead to ground potential for all ranges.

The good zero voltage stability and low-current drain from the measured circuit (on voltage measurements) suggested the use of this amplifier for the determination of pH (hydrogen-ion concentration), especially where a high-resistance electrode is used. For example, with a commonly used type of "glass" electrode, the resistance may be 1,000 megohms at 0 degrees centrigrade; so that a current drain of only 6×10^{-12} amperes gives a drop of 6 millivolts or 0.1 pH, which may

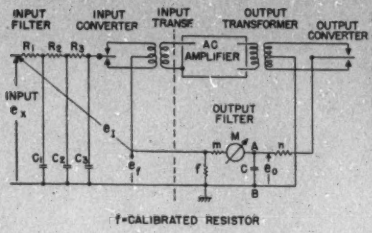


Figure 3 (left). A circuit for voltage measurement at medium impedance, using a step-up input transformer and typical circuits for converters and filters

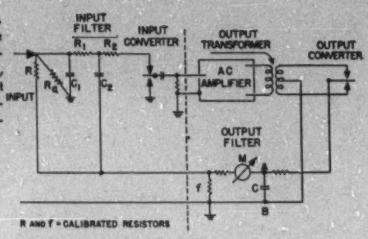


Figure 4. A circuit for current measurement at high impedance using *RC* coupling to a-c amplifier

The input tap on *R* provides for range changing

not be a negligible error.[14] The amplifier circuit used is similar to that of Figure 3 except that the input converter circuit is the type using *RC* coupling to the amplifier as shown in Figure 4, in order to get high impedance. Calculations and measurements check the input impedance to the converter circuit to be about 160 megohms, and since the feedback reduces the voltage across this to about 160 microvolts, the current drain can be held to 1×10^{-12} amperes, from this source. Current from the grid of the first amplifier tube is not a source of error, because of the series blocking capacitor in the converter circuit, Figure 4. Precautions are required in the construction of the vibrator-type converter, Figure 9, to hold spurious currents below 6×10^{-13} amperes, but recently built converters have been giving currents not greater than 1×10^{-13} amperes. The input filter in this amplifier for pH determination consists of only one section, a one-megohm resistor and a 100-micromicrofarad capacitor. Since the voltage ranges are high (at least 0.12 volt) and since the glass electrode systems are normally shielded, this filter, which is effective only for higher frequencies, appears to be adequate. The complete circuit provides for other details such as grounding of the electrode system, compensation for asymmetry voltages in the electrode system, and compensation for temperature coefficient of the electrode system; but these details are beyond the scope of this paper.

Zero Stability

No amount of gain stability obtained through feedback will stabilize the zero. Feedback neither helps nor hinders zero stability. The general equation for any amplifier is

$$\text{Input}\times\text{gain}=\text{output}$$

For consideration of a disturbing input to the amplifier, this equation should be rewritten as follows

$$(\text{Input wanted}+\text{input disturbing})\times\text{gain}=\text{output}$$

From this equation it is clear that in the presence of a disturbing input (noise) and with the wanted input (signal) at zero, the output cannot be reduced to zero by stabilizing the gain. Disturbances may enter at points other than at the input terminals (for example, after the first stage of amplification), but they are generally measured in terms of their equivalence at the input terminals.

The use of d-c to a-c conversion, so that a-c amplification may be used, is the greatest single factor in stabilizing the zero.[1–2,6–13] This procedure avoids the conventional d-c amplifier, with its inherent zero offset and its inherent tendency to zero drift so that compensation for the zero offset can only be temporary. This zero drift results from slow changes in the tubes. Similar slow changes take place in the tubes in the a-c amplifier also, but they are attenuated by the conventional *RC* network between tubes as shown in Figure 6. The changes are further attenuated in the output transformer and finally because of the balanced arrangement of the output converter circuit are reduced to a magnitude negligible compared to the signal at this point. The effectiveness of the elimination of these low-frequency disturbances is proved by the immunity of the amplifiers to changes of power-supply voltage.

Other sources of zero disturbance are not all as easy to subdue. Such disturbances may be of an electric, magnetic, mechanical, or thermal nature. They may also be classified according to their point of entrance, namely, external to the amplifier, within the amplifier, in the d-c circuits, or in the a-c circuits. Still another classification might be made on the basis of the frequency of the disturbing currents or voltages, such as radio frequency, 60 cycles and its harmonics, or direct current with variations up to several cycles per second.

Electric Zero Disturbances

The low-frequency changes in the tubes, had they not been rendered harmless, as mentioned before, might be classified as electrical zero disturbances.

The signal, as it passes through the a-c amplifier, is in the form of a 60-cycle voltage; therefore, any 60-cycle voltage entering the amplifier other than the signal, may disturb the zero. Of course, if it has an exact quadrature phase, it will not disturb the zero; but that possibility is remote, and the only safe practice is to exclude all 60-cycle disturbing voltages regardless of phase. The power supplies for the electron tubes must be carefully filtered of 60 cycles. This applies to the heaters also, requiring that this supply must first be rectified. Heavy 60-cps currents often flow in ground paths. The voltages developed must be kept out of the meshes of the early amplifier stages. There are points of high impedance at the front end of the amplifier, and these must be provided with adequate electric shielding.

Ahead of the converter the signal is in the form of direct current; however, the

Figure 5. Amplifier (with indicator in case).

The range is normally $\pm5\times10^{-10}$ amperes, but the dial at left gives ranges for larger currents

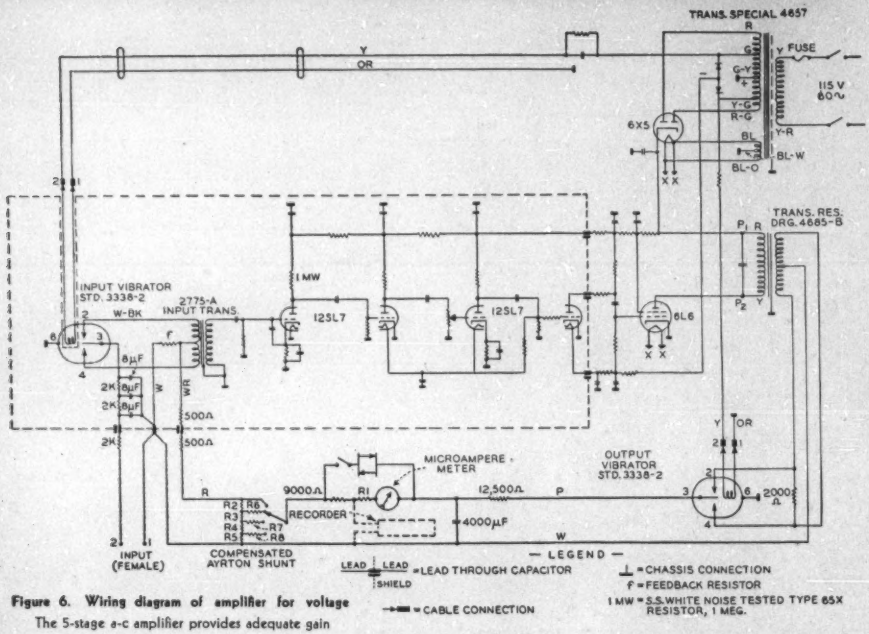


Figure 6. Wiring diagram of amplifier for voltage

The 5-stage a-c amplifier provides adequate gain

converter will pass along a certain amount of 60 cycles as a result of the lack of perfect balance in the input converter circuit. This requires the use of a filter to reduce the 60 cycles coming in on the wires with the d-c signal. The later stages of this filter must have electric shielding against 60 cycles. This filter takes different forms for voltage and for current measurement, Figures 3 and 4. Since this filter enters partly into the path of the feed-back signal, it must be so desiged as to give proper transmission of this signal.

The whole input circuit, external and internal, must have electric shielding against very low-frequency electric effects such as the approach of a charged object (operator's hand). This is of greatest importance in high impedance circuits.

Neither side of the external input circuit may be grounded or permit of grounding. One way to meet this situation is to insulate the chassis from ground.

Although a steady direct current from the first grid cannot reach the input converter, variations of this grid current may reach the input converter, become modulated, and so cause trouble. Reduction of the absolute value of the grid current by the choice of the input tube and its bias is an obvious step. Resistance-capacitance networks between the grid and the converter, as shown in Figure 7, prevent the low-frequency grid-current variations from reaching the converter while they freely pass the 60-cps signal from the converter to the grid.

Radio frequency disturbances are common because all forms of switching on the power line generate considerable amounts of power, spread over the whole radio frequency r-f spectrum. If this radio frequency reaches the electron tubes, or even the contacts in the converter, a 60-cycle component may be developed. The r-f path may be described as electric or electromagnetic, but in any case, the protection against it is a tight, high-conductivity shield surrounding the critical tubes and the converter contacts; r-f filters must be used on all wires penetrating the shield.

Magnetic Zero Disturbances

Loops in the early amplifier stages may link with a 60-cycle magnetic field, originating in the power transformer or some external source, and so produce 60-cycle voltages of disturbing magnitude. The external circuit and early meshes of the input filter can tolerate larger induced 60-cycle voltages and from this point of view could tolerate larger loops. However, there are likely to be present slowly varying magnetic fields, and these, when loops are present, will produce low-frequency voltages which can in no way be distinguished from the signal. For voltage-sensitive amplifiers, therefore, the whole input circuit should have minimum loop size and/or be enclosed in a magnetic shield.

The early stage amplifier tubes are subject to magnetic fields, possibly the "magnetron effect," and to protect against such fields a magnetic shield is used on the first tube.

The input transformer, being a magnetic device operating at the lowest power level in the amplifier, must be well protected. It is of the core type humbucking construction, and has four concentric high permeability magnetic shields,[15] as shown in Figure 8. Figure 9 shows the vibrator-type converter. The strong field of the 60-cycle drive coil is kept away from the contacts and measuring circuit by a magnetic shield in the form of a long permeable iron skirt. In addition, all loops are kept small.

Mechanical Zero Disturbances

The item most susceptible to mechanical effect is the first stage amplifier tube. Severe table shocks formerly produced a wide band of frequencies in the output, so that there was generally enough 60 cycles to cause a disturbance. Some tubes are more susceptible than others, but a rubber-mounted socket has proved adequate to protect against all ordinary shock or vibration. This socket is also of value in avoiding oscillations from mechanical feedback.

Thermal Zero Disturbances

Current from the first grid is a function of the cathode temperature, therefore when grid current is a disturbance, cathode temperature is a factor. It is, of course, only changes of temperature which produce any effect, and even this effect can be made negligible by proper attention to minimizing the grid current effect as previously discussed under "Electric Zero Disturbances."

Thermoelectric voltages are the worst source of disturbance arising from thermal causes. There are only two methods of attack

1. To use metals giving the lowest obtainable thermoelectric power at all junctions.
2. To minimize temperature differences between thermoelectric junctions.

Since copper must be used in some places, notably the transformer primary, materials have been chosen to match copper as closely as possible. Where the circuit passes through the "button capacitors" of the r-f filters, it is preferable to drill the studs so that an unbroken piece of copper wire can pass through. It should be tacked on at one end only with a minimum amount of solder. Copper resistors are best where the exact resistance value is not critical. When necessary, manganin resistors can be built so that the copper-manganin junctions are more nearly at the same temperature. A special solder developed by General Motors[11] has a low junction potential against copper and has been found very helpful.

Steps taken to minimize temperature difference between thermoelectric junctions consist of placing the complementary junctions in close thermal proximity, and then making the paths for heat flow to and from the junctions thermally long and as nearly equal as possible.

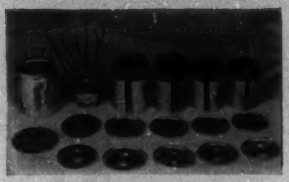


Figure 8. Input transformer (exploded view)

The four highly permeable magnetic shields make it immune to stray fields

Gain Stability

Adequate Gain

When it is stated that the gain is stabilized by the use of over-all d-c feedback, it is the net gain which is stabilized. Referring to Figure 3, the net voltage gain is e_0/e_X.

The amplifier for infrared may be taken as an example. The resistor f was 0.5 ohm and the sum of $m+M$ was 10,000 ohms, therefore when e_0 had a steady value of one volt, e was 50 microvolts. The a-c amplifier had a voltage gain of 32,000,000 but this was multiplied by an input transformer stepup of ten and divided by a loss in the input filter, the converters, the output transformer

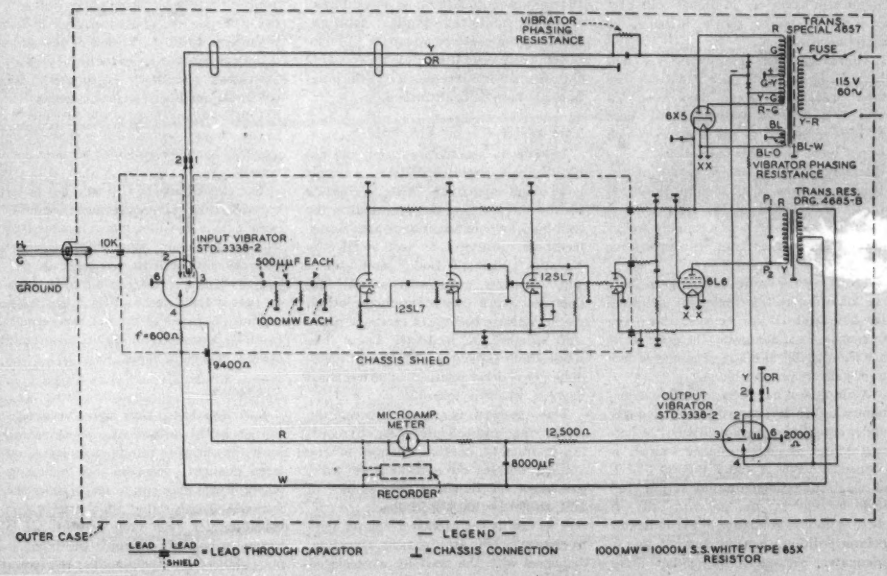


Figure 7. Wiring diagram of amplifier for pH

The 1,000-megohm input-grid resistors help to maintain high input impedance

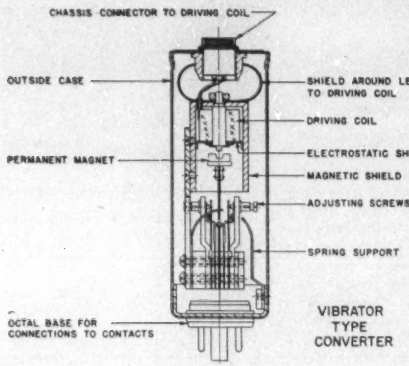


Figure 9. Vibrator type converter

A permanent magnet on the central reed is driven by an electromagnet above it

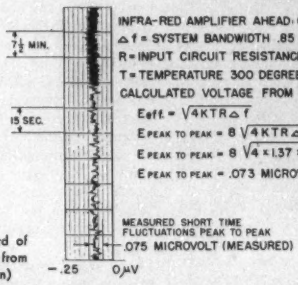


Figure 10. Record of noise (principally from thermal agitation)

and output filter of about eight, so that the forward gain e_0/e_I was about 40,000,000. In particular, under the assumed condition of one volt for e_0, the error voltage $e_I = 1/40{,}000{,}000 = 0.025$ microvolt, which was negligible (one part in 2,000) compared to the 50 microvolts of e_I or the 50.025 microvolts of e_X.

A two to one reduction of the forward gain e_0/e_I from 40,000,000 to 20,000,000 (as from a severe reduction in supply voltage) would have increased the error voltage e_I to 0.05 microvolt, but this would still have been negligible (one part in 1,000 compared to the input e_X of 50.05 microvolts.

However, if the forward gain e_0/e_I had been designed to be only 2,000,000, the error voltage e_I would have been 0.5 microvolt, which is not negligible (1 part in 100) compared to the input e_X which would have been 50.5 microvolts. Furthermore, a two to one reduction of forward gain from 2,000,000 to 1,000,000 would have increased the error voltage e_I to one microvolt, which roughly would have added another 1 per cent error for a total error of 2 per cent.

The foregoing figures are given to show the importance of designing for adequate forward gain. It can be seen that if the forward gain is adequate, the error voltage is negligible and large changes of forward gain are permissible.

A simple test has been devised to determine whether the forward gain is adequate under actual working conditions. A current source (high impedance source) is connected across A and B Figures 3 or 4, of magnitude calculated to supply full scale current to the indicator M. If there is no visible change in the indicator pointer position, then the forward gain is adequate. The proof is simple. Since the indicator deflection did not change, the output from the amplifier must have taken a full scale swing, by way of compensation, and so also the amplifier input or error voltage e_I. Since this full scale swing of e_I caused no noticeable change in the indicator deflection, e_I must for all operating values be negligible; hence the forward gain must be adequate. A convenient feature of this test is the fact that the external circuit connected to the input does not have to be disturbed.

Oscillation and Its Prevention

In order to use adequate gain and not be troubled with oscillation, a single large-shunt capacitor C (with appropriate resistors), Figures 3 and 4, is used in the feed-back loop, in accordance with simple feed-back practice as set forth by Nyquist,[4] Vance,[16] Bode,[8] and others. The capacitor attenuates at the frequencies where oscillation might otherwise take place because of excessive phase shift around the feed-back loop. The single-shunt capacitor itself cannot introduce phase delay in excess of 90 electrical degrees, which is tolerable.

The capacitor is placed between the power stage and the indicating or recording instrument, so that changes in line voltage, working through the power tube, can affect the indicator only slowly, in fact, so slowly that the feedback can correct as fast as the capacitor voltage tries to change. This arrangement, therefore, dispenses with the need for a regulated power supply. It does require the use of a relatively high power output tube, because this tube must supply not only the steady-state indicator current, but the charging current for the large capacitor which, for full scale changes, may be forty times the steady state current.

The capacitor C is electrolytic in order to save size. Electrolytic capacitors are current sources.[6] If, however, the test, for adequate gain is met, then the current from the capacitor will do no harm, provided only that it does not exceed the full scale current of the indicator.

A close inspection of Figures 3, 4, 6 and 7 will disclose that the input filter enters partly into the feed-back loop. It will be noticed that the resulting phase shift is in the direction of phase advance which is the safe direction, in view of the phase delay contributed by capacitor C and its associated resistors. Fortunately, the converter-amplifier-converter system does not develop any large phase advance at low frequency, as does the simple-multistage RC-coupled amplifier without converters.

The capacitance of C should not be increased greatly above the value needed to permit the use of adequate gain, since that would needlessly slow the response. Experience shows that it is generally possible to obtain a stabilization of net gain to better than one part in 1,000 while retaining a response time of two seconds (for 99.9 per cent of full deflection after an abrupt change of input).

Overload

It is desirable to have high power capabilities in the output tube so as not to limit the charging rate of capacitor C on large changes. For the null indicator, where inputs may run to many times the normal full scale value, the power tube is called on for even more output. If it cannot supply this output, the input is not balanced and the amplifier receives a

sustained overload, so that rapid recovery is difficult.

Unwanted Feedback

The existence of the high gain in the amplifier requires that feedback, other than that intended, must be kept very low. Since this is a matter simply of good electronic engineering, details are covered by title only

1. Filter and decoupling on B supply.
2. Filter on heater supply.
3. Ground coupling.
4. Coupling from direct capacitance.
5. Shockproofing against mechanical feedback.
6. Heater temperature effects.

Phase Shift With Overload

In an electronic recorder[1–3,6] using a slide-wire potentiometer to balance, the largest unbalance does not generally exceed the minimum significant unbalance by a factor of more than 2,000. In a feed-back amplifier, however, a much larger factor may be encountered, especially during the first few seconds of warm-up. It is of vital importance that the phase of the 60-cycle signal going through the amplifier shall not be shifted with these large overloads to the point where the converter starts working in the wrong sense. When this happens the system locks itself off to one side and stays there until power is turned off. By following the circuit of Figures 6 and 7, this difficulty may be avoided.

The result is accomplished by providing for proper limiting action in the output tube and then, for increasing signals, in the driver stage, the predriver stage, and so forth.

Resistors and Instrument Used

The net gain stability can be no better than the stability of the calibrated resistors, in the feed-back loop, f in Figure 3, f and R in Figure 4. The resistor f is no problem. The advantage of being able to use wire-type manganin resistor at R for the amplifier for current has already been discussed under "Circuit Variations and Applications."

Obviously, the measurement can be no better than the indicating or recording instrument used. Most of the amplifiers made have been provided with built-in indicating instruments rated at ±1 per cent When it is desired to work more closely to the full stability available in the amplifier, an external instrument, generally of the null balance type, can be used. It can be a current-measuring instrument connected in series with the internal indicator or a potential-measuring instrument connected as the recorder shown in Figures 6 and 7.

Noise and Response Time

Thermal Agitation

Under the heading "Zero Stability" many forms of disturbance were discussed. The term "Noise" has been carried over from the language of the communication art to describe these disturbances. Most of these sources of noise are man-made, and can be reduced by precautions. However, the noise resulting from thermal agitation is of natural origin, and sets a natural limit to noise reduction. Figure 10 shows this noise as random fluctuations of the pen of a recorder connected to the infrared amplifier with the RC input filter previously described. The measured peak-to-peak noise of 0.075 microvolt is in good agreement with the 0.073 microvolt predictable from the work of Johnson,[17] Nyquist,[18] Landon,[19,21] and Jansky.[20]

Reduction of this noise in terms of input voltage equivalent can come only from

1. Reduction of the temperature of the noise-making resistance.
2. Reduction of the value of the noise-making resistance.
3. Reduction of the frequency band width of the system.

Refrigeration would be required to work on factor 1.

Factor 2, the resistance value has been varied by way of experiment. Since the noise-making resistance of Figure 10 was principally in the input filter, and since it was desired to retain the same 60 cycles per second filtering for certain applications, the 2,000-ohm filter resistors were increased to 2,500 ohms and then shunted experimentally with inductors of 22 henrys and 41 ohms d-c resistance. This reduced the noise-making resistance from about 6,240 ohms to about 360 ohms, changed the measured band width from 0.84 to 1.49 cps, and changed the measured peak-to-peak noise from 0.075 to 0.030 microvolt. This was in good agreement with the value of 0.024 microvolt calculated from the new noise-making resistance, and the new frequency

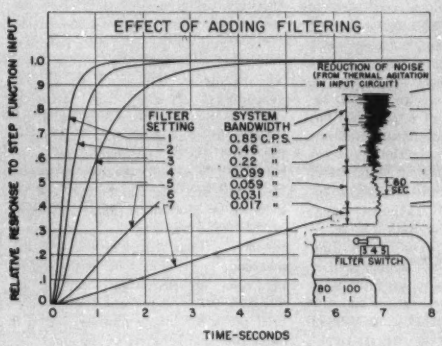


Figure 11. Effect of switching in low-frequency filtering at the recorder

The random noise is reduced, but the response time of the system is lengthened

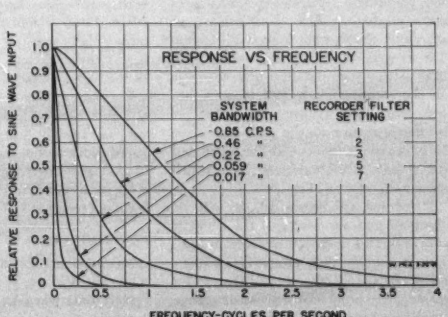


Figure 12. Effect of switching in low-frequency filtering at the recorder in order to reduce the random noise

Sine wave inputs were used to obtain the curves. The curves are used to calculate the bandwidth of the system for each filter setting

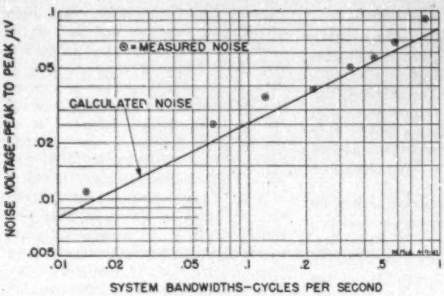


Figure 13. Noise versus bandwidth, measured and calculated for 6,000 ohms at 300 degrees Kelvin

A reduction of band width of about 100 to 1 reduces the noise (from thermal agitation) by about 10 to 1

band width. The noise-making resistor has been further reduced in other set-ups by dispensing with the filter and by using an input transformer of greater step-up and lower primary resistance. In particular, with a 2-ohm noise-making resistance (the transformer primary resistance) and a band width of about 1 cps, the peak-to-peak noise was measured as 0.0017 microvolt compared to a calculated value of 0.0014 microvolt. Although this was working to a lower voltage level, it was not working to any lower power level.

Factor 3, the band width, was the most convenient factor to alter, since the recorder was equipped with a group of *RC* filters connected through a dial switch to reduce its speed of response,[6] and so narrow the band width of the recorder, and hence the band width of the system as shown on Figure 11. The simple *RC*-input filter was used ahead of the d-c amplifier so that its three 2,000-ohm series resistors combined to form a 6,000-ohm noisemaker. The effective band width of the system was measured by taking frequency response curves, Figure 12, squaring the ordinates, integrating under the curve, then dividing by the maximum response, which occurred at zero frequency. Calculated noise figures were plotted against band width, as shown by the straight line on Figure 13. The experimental noise figures for each filter setting were obtained from readings on the recorder chart and these values were also plotted against band width. It is interesting to note the good agreement of the measured points with the calculated curve, even to the narrowest band width measured, only 0.014 cps.

Returning to Figure 10, it will be observed that the center of the curve is only about 0.1 microvolt offset from zero. This is not the result of any bias adjustment, but simply the result of the careful construction previously discussed. Some amplifiers have had a larger offset from zero, but 0.5 microvolt appears to cover the worst amplifier under the worst condition. A goal which might be set would be to reduce the zero offset to the calculated peak-to-peak noise. As previously stated, this figure for the conditions of Figure 10 was 0.073 microvolt so the worst zero offset was greater than the calculated noise by a factor of 7. As the resistance of the circuit is reduced, the thermal agitation noise voltage is reduced,[16,11] but the zero offset can be reduced only with great difficulty, because of the thermoelectric effects. The result is that the ratio of zero offset to calculated peak-to-peak noise becomes greater and the amplifier is, in this sense, farther from the natural limit. As the resistance of the circuit is greatly increased, the thermoelectric effects become relatively less of a problem, but other problems enter. These problems are related to contact potential differences and capacitance effects, but in any case, they produce zero offset which again becomes large compared to the calculated peak-to-peak thermal agitation noise, so that again the amplifier is farther from the natural limit. The amplifier used in obtaining Figure 10 (amplifier for voltage with 6,000-ohm input filter) has the lowest ratio of zero offset to calculated peak-to-peak noise of the various amplifiers mentioned, and hence appeared to be the best example for this paper.

For some applications the zero offset is not objectionable, provided it does not vary too rapidly. In other words, these applications do not require measurement of the very low frequencies (such as a cycle per day); in brief, a true d-c amplifier is not required.

Preferred Forms of System Response

As previously explained, Figure 11 shows the response of the system plotted against time when an abrupt change of input is applied (unit step function). The different responses result from the different filter settings in the recorder, numbered 1 to 7. Figure 12 shows the response of the system plotted against frequency when sine-wave inputs are applied. The different responses result from the different (numbered) filter settings in the recorder. For each filter setting therefore, the time response has been experimentally determined, and independently the frequency response has been experimentally determined.

The amplitude at which the frequency-response curves were taken was low enough to be within the linear range, that is, below the range where limiting of any kind is significant. Limiting may occur in the amplifier or in the recording instrument which completes the system, and must therefore be considered a part of the system. Direct-deflection instruments are not inherently subject to limiting; however, in making them into recording instruments, complications enter. Photographic recording from a mirror on a suspension-type instrument is preferable. Direct-writing instruments have friction, which means they are nonlinear in a way that is most noticeable at small amplitudes. For the work here reported instead of a deflection instument, an electronically-balanced null-type recorder was used, which had a full scale balance time of 0.8 second on the No. 1 filter position. With this minimum filter setting there was definite speed limiting set by the synchronous speed of the balancing motor as apparent in the response of the recorder alone, Figure 14.[6] This limiting speed was at the rate of full scale travel in 0.62 second. A study of Figure 11 shows that the maximum speed is reduced as filtering is added (to the recorder, and hence to the system), and for filter settings 2, 3, 4, 5, 6 and 7 the maximum speed is held below the limiting value so that speed limiting is avoided. There may have been a very brief period of torque limiting but because the time constant of the amplifier input filter combined with the time constant of the recorder input filter, the recorder was called upon only for a finite value of acceleration which probably fell within the torque limit of the motor. For filter settings 2, 3, 4, 5, 6 and 7, therefore, the system was linear for any on-scale amplitude. A study of the curve for filter setting number 1, Figure 11 reveals that the maximum speed is at the rate of completing the pen travel in about 0.62 second. If the change is large therefore speed limiting will occur and the system action will be nonlinear. This complicates the analysis, also it adversely affects

the signal-to-noise ratio as will be discussed later.

Where the system is linear, it is not too difficult to apply analytical methods to relate band width to response time provided the time response has a simple analytical form. In particular, a study of Figure 11 shows that the time response of the system, especially for the large filter settings, was very close to being a simple exponential as given by a single *RC* circuit.

Another form of response commonly encountered is that of the critically damped system as given by an *LRC* circuit or the mechanical equivalent, a galvanometer with inertia, damping and suspension stiffness. It also has a definite analytical form.

These two forms of response are plotted against time in Figure 15. For comparison purposes a band width of 1 cps has been selected and the time response curves have been calculated and plotted on this basis. The simple exponential response is above that of the critically damped response up to a response of 0.72 of the full or unity value. For a null indicator or a controller, the simple exponential might, therefore, be preferable. For measurement, however, where a value must be determined to ±28 per cent or less, the critically damped form of response is faster. Countless other response forms could be considered but the one of greatest interest is the ideal response for measurment purposes. The third response on Figure 15, which we will call the terrace response, was suspected as being this ideal response, since it weights all times equally through the measuring time. The proof of this is covered in the Appendix.

Figure 15 also gives, in tabular form, the time required for the three forms to reach the half-way point and other points short of completion. Added columns give the ratio of the figures to those of the terrace response. In particular, for 0.999 completion the critically damped requires 2.32 as much time as the terrace form, and the simple exponential requires 3.46 as much time as the terrace form. For only 0.990 completion the critically damped requires only 1.68 as much time as the terrace form, and the simple exponential only 2.32 as much time as the terrace form. From these figures we might conclude that the simple exponential is not too slow for measurement to ±1 per cent.

Although the "terrace response" is an ideal response not physically attainable, it may be approached by proper circuit design.

Figure 14 shows a curve which might be thought to approach the terrace response until it is recalled that it was obtained under a speed limited condition. For smaller changes this recorder gives the same speed rather than the same balance time, as it would if it had a true linear terrace form of response. Actually, for small unbalances the speed limiting drops out and the recorder is essentially linear with a short balance time, wide frequency band, and high noise susceptibility. In spite of this, it takes extra time to measure large abrupt changes requiring nearly full scale pen travel because of speed limiting. In brief, it discriminates against the signal, which is generally large, in favor of the noise, which is generally small. Fortunately, the recorder of Figure 14 has a relatively high pen speed before limiting sets in. Sufficient recorder filtering can be added to establish operation in the unlimited or linear range. This results in a system of still moderately high speed, (2.5 seconds), Figure 11, filter No. 2, where there is no discrimination against the large signal in favor of the small noise.

Conclusions

By following certain methods and taking certain precautions as described herein d-c amplifiers can now be built with adequate stability of zero and gain to be used with precision ahead of precise indicating or recordng instruments to measure smaller voltages or currents.

Appendix

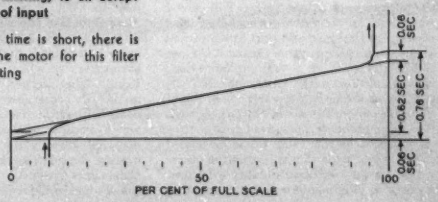
Theoretical Considerations Concerning Noise

Thermal Noise Frequency Distribution

The thermal agitation of electric charges in conductors of the input circuit, prior to the first amplifier tube, is by far the most important source of random noise within the amplifiers discussed in this report, the noise of the first tube being small in comparison. In 1928, J. B. Johnson[17] reported the first measurements of such thermal effects, which frequently are called "Johnson noise."

Figure 14. Time response of the recorder alone (with minimum filtering) to an abrupt change of input

Although the balance time is short, there is speed limiting from the motor for this filter setting



Using the second law of thermodynamics and the laws of a loss-free transmission line, Nyquist[18] has established the following equation for the frequency distribution of the thermal agitation voltage

$$E_f^2 df = 4R_f kT df \qquad (1)$$

In equation 1, k is Boltzmann's constant (1.374×10^{-23} *watt-second per degree Kelvin*), T is the absolute temperature in degrees Kelvin of R_f, and R_f is the resistive component of the impedance. The equivalent noise-making resistance in the amplifier application is the resistive component, R_f, of the impedance on the input side of the first tube, including that of the source of voltage which is being amplified.

Response of a Linear System to Thermal Noise

In interpreting the noise disturbances at the output of a linear system in terms of a theoretical noise voltage at the input, it is necessary to know the system response to given types of input signals. One approach to this problem is to determine the steady-state system response to sinusoidal voltages of unit amplitude and various frequencies. Let such voltages be impressed on a system consisting of input filter, amplifier and recorder. The amplitude of the recorder deflections as a function of frequency constitute the electromechanical frequency response $Y(f)$ of the system. The mean-square-output deflection d^2, due to input-thermal noise, can now be expressed as[18]

$$d^2 = 4kT\int_0^\infty R_f[Y(f)]^2 df. \qquad (2)$$

Equivalent Band Width

When the filtering is low-pass, as in the case of systems intended for the measurement of direct current and slowly varying signals, the maximum value of $Y(f)$ occurs at zero frequency. This value, $Y(O)$, is simply the d-c voltage sensitivity. The mean-square deflection, d^2, divided by $[Y(O)]^2$, gives the equivalent mean-square-noise voltage, e^2, at the input terminals. When R_f is substantially a constant, R, over the range of frequencies to which the system responds, division of equation (2) by $[Y(O)]^2$ gives

$$e^2 = 4kTR(\Delta f). \qquad (3)$$

The quantity Δf is known as the "equivalent band width." It may be expressed in terms of the relative frequency response, $y(f) = (Y(f)/Y(O))$, as follows:

$$\Delta f = \int_0^\infty [y(f)]^2 df. \qquad (4)$$

Analysis of Noise Traces From A Recorder

The Peak-to-Peak Method of Analysis

In the development of amplifier-recorder systems, comparison of observed output fluctuations with those predicted by equation 3 indicates whether or not all other noise source effects have been made negligible with respect to Johnson noise. Unfortunately, the mean-square-recorder deflection is not easily determined directly from the pen trace, a somewhat involved analysis being required.

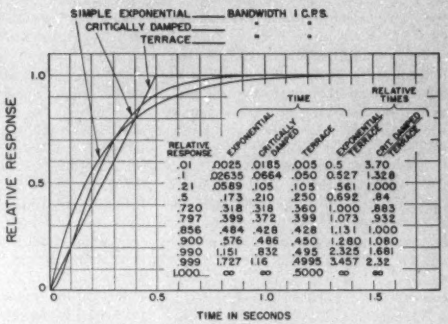
Experimental studies[15,20] of the amplitudes of recorder fluctuations due to Johnson noise have led to the conclusion[20,21] that the ratio of the maximum peak-to-peak spread to the root-mean-square value may be taken as 8.0, if one excludes occasional peaks that go considerably higher. The noise traces upon which Figure 13 of this report is based were taken for ten to twenty minutes on each filter setting. They were then divided into two-inch or four-inch sections, corresponding respectively to one minute or two minutes of recording, the longer sections being used with the lower band widths. The peak-to-peak value for each section was then measured. To improve the statistics, particularly at the lower band widths, measurements were also made for new sections defined by the midpoints of the old sections. The sectioning procedure is necessary in order to leave out of account long-term drift not arising from Johnson noise. For each band width the maximum measured peak-to-peak value was then plotted as shown in Figure 13. For comparison, a graph was also plotted of the peak-to-peak values found by multiplying the theoretical rms noise of equation 3 by the factor 8, for each band width. The agreement of the experimental points with the calculated graph has been discussed elsewhere in this report. It may be added that this agreement supports the use of the factor 8.

Limitations of the Peak-to-Peak Method

The conversion of peak-to-peak values to rms values by means of a fixed ratio regardless of band width should be done with caution.[21] The lower the band width, the greater is the chance of error, since the number of peaks on a given duration of trace is smaller. Landon[21] has shown that the amplitudes of the fluctuations in the output of a linear system in response to Johnson noise at the input are distributed in size according to the normal error law. He calculates upon this basis the expectation that the peak-to-peak output voltage will exceed eight times the rms value during only 0.0032 per cent of the time, and that it will exceed seven times the rms value during 0.134 per cent of the time. It is very probable that, in the peak-to-peak measurements, the peak-to-peak spreads greater than eight times the rms values would not be observed. Also quite probably, spreads of greater than seven times the rms value would be missed at the lowest band widths, unless the observation record were considerably extended. A factor of judgment is apparently required concerning whether or not the exceptional spreads are occurring as much of the time as should be expected from their normal distribution.

Figure 15. Forms of time response

All three forms are calculated and plotted for the same bandwidth hence would yield the same noise susceptibility. The "terrace" form is an ideal form useful for comparison purposes



FORMS OF RESPONSE

Sampling Method Based Upon the Normal Error Law

An alternative procedure for determining the rms value from a noise trace, which does not depend upon the use of an assumed peak-to-peak rms ratio has been developed. This method requires that the recorder chart speed be made great enough that the widths of peaks can be measured under a magnifying glass along a few inches of trace, for various levels above and below an estimated average or center line of the noise trace. The fraction, $2p$, of the time spent by the deflection outside of two lines of separation Δd, lying parallel to and on either side of the center line, is determined. Entering with the value of p into a graph of the function

$$p = \frac{1}{\sqrt{2\pi}} \int_r^\infty \epsilon^{-\frac{r^2}{2}} dr \qquad (5)$$

as given in Figure 1 of Landon's paper,[21] where

$$r = (1/2)\frac{\Delta V}{rms}$$

and ΔV is the equivalent in volts of the separation Δd between the two chosen lines, one reads the value of r. The rms value of the noise voltage is therefore $(1/2)\Delta V$ divided by r.

One advantage of this method is that it can supply a greater amount of reliable statistical data from a small length of suitable trace than can the peak-to-peak method. This is true because it utilizes the more abundant fluctuations which exceed 7, 6, 5, and so forth, times the rms value, in order to establish the distribution-function parameter, r, which determines the rms value. The method therefore does not necessarily break down, as does the peak-to-peak method, in the case of a noise record which, because of a statistical fluctuation, does not contain any of the peak-to-peak voltage fluctuations which are as great as eight times the rms. By sampling at additional and smaller values of the line separation Δd, a consistency in the calculated rms values is established; that is, they converge to a limiting value for the rms. It may be shown that this limiting value is simply

$$rms = \frac{1}{\sqrt{2\pi} \times \text{max. slope of } P_{obs} \text{ vs } V} \qquad (6)$$

Equation 6 allows the rms value of the random trace to be obtained directly from the maximum slope of a graph of the observed fractional time, P_{obs}, spent to one side of a line parallel to the trace (and corresponding to V microvolts), plotted against V. It will be noted that in making the measurements a grid of V-lines should be drawn parallel to an estimated center line of the trace. The actual center line may be determined later, by the point $p = 1/2$ on the resulting graph. An example of this analysis upon the high speed portion of the trace shown in Figure 10 is summarized in Table 1.

Relation of Band Width to Time Response

Time Response Slope Versus Band Width

Let a represent the system response, relative to the ultimate value, as a function of the elapsed time, t, after application of a unit-step type of signal. It then can be shown that

$$\Delta f = \int_0^\infty [y(f)]^2 df = (1/2) \int_0^\infty [a'(t)]^2 dt, \qquad (7)$$

where

$$a'(t) = \frac{d\,a(t)}{dt} \qquad (8)$$

is the slope of the relative time response characteristic. This fundamental relation for linear systems was first cited, in slightly different form, by Carson and Zobel.[22] These authors emphasized the frequency response aspect of the relation, but made no use of the right-hand integral.

Band Widths for Simple Forms of Time Response

Application of equation 7 to several simple and interesting cases offers material aid

in qualitatively analyzing the effect of the shape of the time response characteristic on the equivalent band width.

The first case (Figure 15) is the simple exponential, the equation for which is

$$a(t) = 1 - \epsilon^{-\frac{t}{\tau_e}} \tag{9}$$

where τ_e is the time to reach a fraction $(1-1/e)$, or 63.2 per cent, of the ultimate value. For this case equation 7 gives the band width as

$$\Delta f = \frac{1}{4\tau}. \tag{10}$$

The second case (Figure 15) is the response which is characteristic of the critically damped galvanometer.

$$a(t) = 1 - \epsilon^{-\frac{t}{\tau_e}}\left(1 + \frac{t}{\tau_e}\right) \tag{11}$$

where τ_e is the time to reach the flex-point at a fraction $(1-2/e)$, or 26.4 per cent, of the ultimate response. For this case

$$\Delta f = \frac{1}{8\tau_e} \tag{12}$$

The last case, Figure 15, has been termed the "terrace function" type of response. It is made up of the following parts

$$a(t) = \begin{cases} 0, \text{ for negative values of } t \\ t/\tau, \text{ for } t \text{ between zero and } \tau \\ 1, \text{ for } t \text{ greater than } \tau \end{cases} \tag{13}$$

where τ is the time to reach the ultimate value. The system band width is

$$\Delta f = \frac{1}{2\tau} \tag{14}$$

Theorem: Minimum Band Width is Provided for a Given Measurement Time by the Terrace Function

Figure 15 shows that, for the same band width, the terrace type response reaches its ultimate value sooner than the exponential or critically-damped type of response. This observation suggests that conversely the terrace function may give the smallest band width of the three response types for the same measurement time, that is, time to reach the ultimate value. Since the exponential and critically-damped functions require an infinite measuring time, it is desirable to investigate the band widths for the three types of response having the same fractional measuring time, for example, the time required to reach 99.9 per cent of the ultimate response. If this time is taken as one second, the band widths are found to be: 1.727 cycles for the simple exponential response; 1.154 cycles for the critically-damped response; and 0.4995 cycle for the terrace type of response.

From these results, and from an application of the calculus of variations to the right-hand integral of equation 7, one can establish the most important characteristic of the terrace function: of all the possible response curve forms, that of the terrace function provides the minimum band width for a given fractional measurement time. It is necessary, for the general validity of this theorem, that the fractional measuring time correspond to a sufficiently large fraction of the ultimate response, so that the tail of the right-hand integral of equation 7 may be neglected from the fractional measuring time to infinity in comparison with the whole integral.

Corollary: The Terrace Function Provides the Shortest Measuring Time for a Given Band Width

Of all response functions providing the same measuring time τ, the terrace function has been proved in the foregoing section to give minimum band width, equal to $1/2\tau$, according to equation 14. The plot of band width Δf against measuring time for the terrace function is therefore the rectangular hyperbola $\Delta f \times \tau = 1/2$. Another terrace function which reaches its ultimate value in a time τ' less than τ must have a band width $1/2\tau'$, which is greater than $1/2\tau$. Also, by the theorem, all other functions which reach the ultimate value in time τ' must have band widths greater than $1/2\tau$. Therefore, any function which reaches its ultimate value in a time less than the time τ required by the terrace function cannot have the same band width, but must require a greater band width. Hence the corollary.

In practice, the infinite accelerations involved make it impossible to achieve a true terrace function response. It may be possible, however, to obtain a sufficiently close approximation, which would reduce band width, hence interference from random noise. Also, since the terrace response represents a definite minimum band width for a given measurement time, higher recording speeds can be achieved only at the expense of increased band width and noise susceptibility. In these respects the terrace function is, at least, a decidedly useful criterion against which actual responses can be compared.

Table I

Δd, In. on Chart (Original)	ΔV Microvolts Equivalent	2ρ, Fractional Time Outside of Δd	Rms Calculated[21], Microvolts	8×Rms for Comparison with Peak-to-Peak
0.295	0.075	0*	0.0089*	0.071*
0.235	0.060	0.0152*	0.0115*	0.092*
0.177	0.045	0.0606*	0.0120*	0.096*
0.114	0.030	0.234	0.0139	0.110
0.059	0.015	0.560	0.0134	0.107
0.029	0.0075	0.758	0.0130	0.104
0.000	0.000	1.000	(0.0120)**	(.096)**

Measured peak-to-peak for fast chart motion (120 seconds duration) —0.075
Measured peak-to-peak for slow chart motion (15 minutes duration) —0.095

*These data (where ρ is $< .04$) should not be given great weight because of meager sampling.
**Calculated from equation 6.

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No Discussion

Improvements in Rolling Mill Preset Screw-Down Controllers

J. D. LEITCH
MEMBER AIEE

C. A. SCHURR
ASSOCIATE AIEE

Synopsis: In the rolling mill screw-down pre-positioning control system discussed in this paper, the use of electronic-balance detectors and anticipators permits higher screw-down motor speeds without loss of accuracy in the final positioning of the rolls. The electronic-balance detectors replace polarized contact-making ammeters while balance anticipators permit the introduction of slow-down motor circuits for accurate stopping. This latter feature could not be provided so readily by the earlier control method. A new system of schedule panels on which one or more complete rolling schedules may be preset is a definite and important improvement over the earlier design. In the new panels, it is necessary to locate only two plugs for each pass per schedule while in the older method employing cords, it was necessary to locate both ends of two cords, making four connections per pass per schedule. Freedom from the complicated network of cords makes the set-up procedure more rapid and certain.

IT IS WELL KNOWN that the quality of a rolled metal product is influenced greatly by the manner in which it is rolled. All other factors being equal, a consistent control of the draft or the reduction per pass will give a product superior to that where no definite rolling schedule is followed. The accuracy of the product from a rolling mill is very important to the satisfactory operation of the succeeding mills. For example, delays can be serious at the first stand of a billet mill if there is no consistency in the separation of the rolls at the final pass of the blooming mill which feeds it.

An automatic preset screw-down controller, in which the entire rolling procedure is prescheduled by some authorized person, increases the accuracy of the final product, reduces abuse of the mill, and gives a consistently better product metallurgically.

The preset screw-down controller using differentially-driven control elements and first described in 1933[1] has found considerable favor in the control of plate mills in the brass, aluminum, and steel industries. The speed and accuracy of the control has been adequate in most cases although there has been some need for higher speeds to eliminate delay in waiting for the rolls to reach their next position, especially on return to the first pass. Any increase in the speed of the roll movement must result in loss of accuracy unless steps are taken to compensate for the additional rotational energy by adding slow-down features to the controller.

Differential Cut-Out Assembly

Before describing the improvements in the controller itself, it is necessary to show how the roll position-indicating means is tied to the screw-down drive. The differential cut-out assembly is shown in Figure 1 along with the gearing of the mill screw-down. It consists of a synchronous-tie receiver, an automotive-differential gear assembly, main and vernier rheostats, a small set-up motor, a cam-limit switch or cutout, and appropriate gearing. This cut-out assembly is a standardized unit which can be used for any rolling mill with very little change, if any.

Referring to Figure 1, any up or down movement of the mill rolls will cause a displacement of the contact arm of the main rheostat and the cams of the cam cutout. Because of the locking action of the 35 to 1 worm-gear reducer between the 1/4-horsepower set-up motor and the differential shaft *c*, the vernier-rheostat arm will not be displaced. The movement of the screw-down motor is under the control of the main rheostat and the cam cutout only. This will be described in detail later.

With the set up motor at rest, the angular movement of the rotating arm of the main rheostat is definitely related to the change in roll separation by choice of suitable gear ratio between the screw-down motor shaft and the synchronous-tie transmitter. With a given total travel, the size of the increments of roll separation is determined by the number of equally-spaced main-rheostat segments. The choices made in Figure 1 are typical of those for a plate mill. The cam cutout makes one complete revolution as the arm of the main rheostat moves from the center of any segment to the center of an adjacent segment. This movement corresponds to 0.5 inch of roll travel. With 30 segments on the main rheostat, the movement of the rolls corresponding to 29/30 of a revolution of the main rheostat arm is 14.5 inches.

With the screw-down motor idle, the 1/4-horsepower set-up motor drives the vernier rheostat, the main rheostat and the cam cutout, since the mill holds the shaft *a* fixed through the synchronous-tie motors. Under this condition shaft *b* rotates at the same speed as shaft *c* but in the opposite direction. With 20 segments on the vernier rheostat, as shown in Figure 1, the maximum movement is limited to 19/20 of a revolution of both the rheostat arm and cam cutout, and also 19/20 of a segment on the main rheostat. Thus, operation of the set-up motor moves the main rheostat arm and the cutout cams backward or forward a fraction of a main increment. The amount of this fractional displacement is preset on the schedule panel which controls the movement of the setup motor. Such a schedule panel is a vernier adjustment in the final positioning of the rolls. The

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J. D. Leitch and C. A. Schurr are both with the Electric Controller and Manufacturing Company, Cleveland, Ohio.

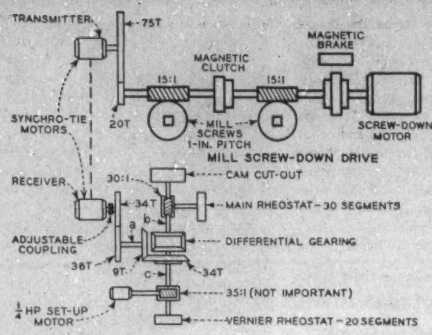


Figure 1. Differential cutout and associated gearing

One inch roll travel = 15 revolutions of screw-down motor
= 15 × (20/75) = 4 revolutions of synchronous-tie motors
= 4 × (34/36) × (9/34) × 2 (differential factor
= 2 revolutions of cut-out shaft
Therefore 1 revolution of cam cut-out shaft = 0.5 inch roll travel
= One segment on main rheostat
Therefore 1 segment on vernier rheostat = (0.5/20) = .025 inch roll travel
Roll travel under automatic control = (29 × 0.5) + (19 × .025) = 14.975 inches

main rheostat is used in conjunction with the cam cutout in controlling the direction and final position of the screw-down motor in accordance with a preset schedule on the main schedule panel. The vernier schedule panel controls the final position of the setup motor through the vernier rheostat. Thus while the main schedule panel may call for 1.5 inch roll separation, the actual separation may be more than this by the amount of the initial displacement of the main rheostat introduced by the movement of the setup motor. This vernier adjustment increases the range of roll travel under automatic control from 14.5 inches to 14.975 inches, the difference being 19/20 of 0.5 inch as provided by 19 increments on the vernier rheostat.

The purpose of the differential gearing and the setup motor is fundamentally that of reducing the number of schedule selection points on the schedule panels. Without the differential gearing, it would be necessary to provide a total of 599 points to cover a range of 14.975 inches in increments of 0.025 inch. With the differential gearing the number of schedule points is reduced to 50, 30 on the main panel and 20 on the vernier. For complete flexibility, it must be possible to connect any one of as many as 25 passes to any one of these points. The number of points on the new schedule panel is reduced from 14,975 to 1,250 per schedule. Actually 25 passes are rarely used, eleven to twenty-one being commonly provided.

Electrical Interpretation of Roll Separation

Referring to the upper part of Figure 2, a 16-segment rheostat is shown along with a 13-pass schedule panel and a thirteen-step ratchet or stepping relay. Fifteen identical resistors are connected in series across the 16 segments of the rheostat to form a potentiometer. A 115-volt a-c supply is connected across these resistors in series with two adjustable resistors. By suitable choice of the 15 resistors and adjustment of the external resistors, it is possible to provide $2^1/_2$ volts between adjacent segments of the rheostat. The 16 segments are connected to 16 horizontal conductors which make up part of the schedule panel.

The stepping relay has a level for the main schedule panel and another for the vernier panel. Its operating coil is so connected in the control circuits that on completion of the roll travel to any preset position, it automatically steps its con-

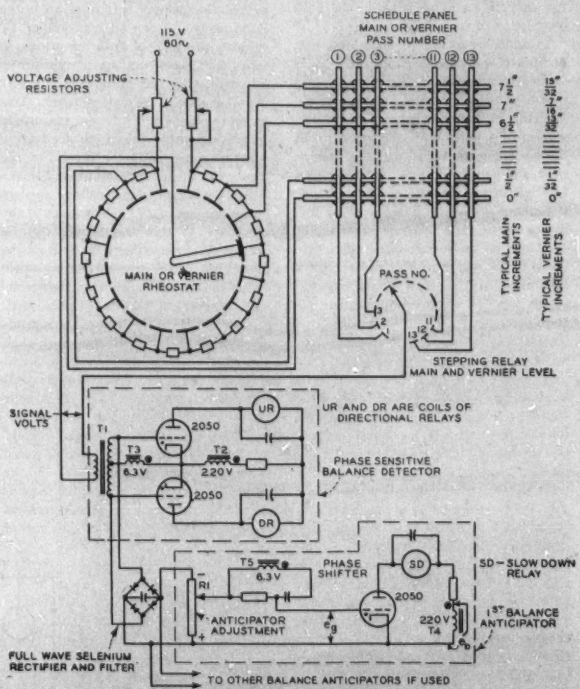


Figure 2. Position indicator, electronic balance detector, and anticipator

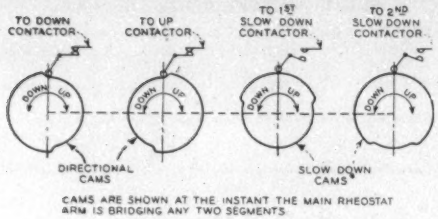


Figure 3. The cam cut-out

tact arm forward to the next stationary contact or pass number. In the case of the setup-motor control, this new position of the stepping-relay arm on the vernier level immediately results in the vernier rheostat being driven to the position called for by the vernier schedule panel. At the same time the stepping relay, through its main level, sets up the necessary circuit for the movement of the screw-down motor to its next position. Such movement does not occur until the operator presses the operating push button after the metal has left the rolls at the completion of the preceding pass.

In addition to the 16 horizontal conductors on the schedule panel, there are 13 vertical conductors, one for each of the 13 passes provided by the stepping relay. Insertion of a plug at any point of intersection of a horizontal and vertical conductor connects them together electrically and schedules some particular pass to a definite roll separation. For example, if number 3 pass is plugged in at $6^1/_2$ inches on the main schedule panel and at 1/32 inch on the vernier panel, the screw-down motor will drive the rolls to give a separation of $6^{17}/_{32}$ inch after the operator presses the operating button for the third pass.

Assuming the schedule panel plugged in for all 13 passes, each position of the stepping relay will electrically connect its contact arm to a specific segment on the rheostat depending on the position of the plug for that pass. It will be shown later how the screw-down motor and the setup motor are brought to their final stopping positions with the main and vernier rheostat arms at the centers of segments.

The electric potential at the rheostat arm is determined by the position of the mill rolls while the position to which the rolls are to move on the next pass determines the potential at the arm of the stepping relay. Thus a signal voltage exists between the arm of the rheostat and that of the stepping relay. The magnitude of this potential difference is directly proportional to the required distance of roll travel while its phase relative to the 115-volt a-c supply, indicates the required direction of travel.

Phase-Sensitive Balance Detector

The phase-sensitive balance detector consists primarily of two thyratrons with a directional relay in the plate circuit of each tube as shown in Figure 2. A transformer *T2* supplies power to each thyratron plate in series with the coils of these relays. Since the transformer feeds the plates of both tubes in parallel, the voltages on the plates of both tubes are in the same phase. A second transformer *T3* supplies the grids of both thyratrons, in series with the secondary of transformer *T1*, with a lockout voltage 180 degrees out of phase with the plate voltages. This lockout voltage is of sufficient magnitude to prevent either tube from conducting when no signal voltage is present at the primary of the input transformer *T1*.

Suppose an a-c signal voltage appears at the primary of *T1*. The voltage at the secondary of this transformer can be considered as two equal voltages from the center tap of the transformer to either end of the secondary winding, these voltages being 180 degrees out of phase with each other. Since one half of the secondary winding of *T1* is in series with each grid, the voltage from the center tap to either end of the transformer winding will add vectorially to the common lockout voltage. In the grid circuit of one of the two tubes this secondary voltage will add to the lockout voltage in the same phase to prevent conduction of this tube. In the grid circuit of the other tube the secondary voltage of *T1* will be 180 degrees out of phase with the lockout voltage and of sufficient magnitude to more than cancel the lockout voltage and cause the tube to conduct. This is true even though the signal voltage appearing at the primary of *T1* corresponds to only one segment or $2^1/_2$ volts because of the turns ratio of transformer *T1*. With one of the tubes conducting, half-wave rectified current will flow through the coil of one relay but not the other. These relays control the direction of movement of the drive.

If the phase of the input voltage is reversed calling for motion in the opposite direction, the opposite tube will conduct. The circuit is thus sensitive to the phase of the input signal voltage relative to the common supply voltage. Closure of relay *UR* or *DR* sets the motor in motion in such a direction as to reduce the signal to zero.

The earlier method of sensing the required direction of travel employed a polarized contact-making ammeter connected across a d-c signal voltage. With direct voltage across the potentiometer and with high ohmic value external adjusting resistors, the voltage per segment was about 0.5 volt. Because of the low resistance of the ammeter shunt, the change in current through the ammeter coil, with change in the number of segments bridged by it, was small. Thus an ammeter sufficiently sensitive to provide contact when bridging two segments could have a coil of adequate thermal capacity to carry the current when bridging the maximum number of segments. The use of a balance detector of such low impedance as an ammeter does not lend itself readily to the introduction of any balance anticipatory control. The low voltage provided per segment necessitates the maintenance of clean contact surfaces on the stepping relay, the main and vernier rheostats, and the schedule panel plugs. In spite of these disadvantages, the use of a contact-making ammeter has proved highly satisfactory on a number of installations.

Electronic Balance Anticipator

Referring to Figure 2, the balance anticipator, as the name implies, is a means of interpreting how closely the

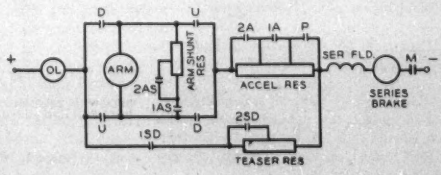


Figure 4. Typical screw-down motor circuit

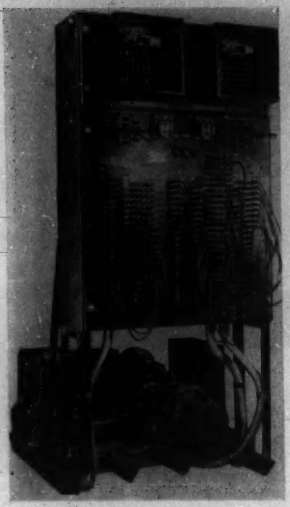


Figure 5. Differential cutout and the old schedule panel

drive is approaching the pre-scheduled position. The anticipator consists essentially of a full-wave rectifier, a potentiometer, a thyratron, a slow-down relay and a phase shifter. The plate of the thyratron is supplied with alternating current from the secondary of transformer *T4* in series with the coil of slow-down relay *SD*.

The grid circuit has two voltage sources, one a-c and the other d-c, connected in series and adding up to the voltage e_g. The direct voltage is obtained by rectifying the output voltage of the signal transformer *T1* with a full-wave selenium rectifier. This rectified voltage is connected to the ends of an adjusting potentiometer *R1* in such a way that the grid of the thyratron will be negative with respect to the cathode. The alternating voltage in the grid circuit is the voltage appearing across the phase-shifter resistor. It is of small magnitude and it functions to provide a more definite operation of the slow-down relay, by insuring full half-wave conduction of the thyratron.

The magnitude of the direct voltage in the grid circuit varies with that of the signal voltage. The voltage from the positive end of resistor *R1* to the position of the adjusting tap will tend to prevent the thyratron from conducting. Thus with a large a-c signal voltage at the primary of the transformer *T1*, corresponding to a movement of several segments, and with the adjustable tap on *R1* set somewhere near the negative end, the anticipator thyratron will not conduct and the slow-down relay will not close. As the rheostat arm approaches the balance segment the signal voltage will decrease in magnitude until the direct voltage in the grid circuit will permit conduction as a half-wave rectifier, closing relay *SD*. If the adjustable tap on resistor *R1* is located at the positive end, the thyratron will conduct and the *SD* relay will close. Thus, by adjusting the position of the tap on resistor *R1*, the *SD* relay can be made to close at any desired number of segments from the balance segment.

The a-c signal voltage is rectified to eliminate the effect of phase reversal with different directions of motion but with fixed phase of plate voltage.

The ingot or slab is usually short during the first few passes and the actual rolling time is of the order of a second or two. When the screw-down motor stops, the setup motor automatically starts, to establish the vernier displacement of the cutout assembly. If the operator should immediately "enter" the metal between the rolls, it is quite possible that the pass might be completed before the setup motor comes to rest. If the operator should then start the screw-down motor at the instant the metal leaves the rolls, the screw-down motor might be delayed in reaching its stopping position until the setup motor has established the vernier adjustment. This delay occurs only in those cases where the vernier-schedule panel calls for a long movement of the vernier rheostat and the main schedule panel calls for little or no change.

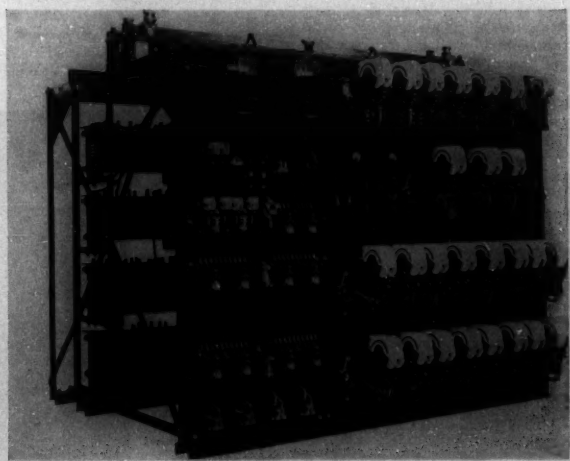
By greatly increasing the speed of the setup motor the delay can be eliminated entirely. The electronic-balance anticipator enabled this to be done without loss in accuracy. It permitted an increase in speed of the setup motor from 400 to 1,750 rpm giving a reduction in setup time from 4 to $1^1/_2$ seconds for maximum travel of the vernier rheostat. The control of the set-up motor is simple because of the constancy of the inertial and frictional characteristics of the system which it drives. By suitable adjustment of the anticipator along with the armature-shunt resistor which it controls, and by proper selection of the final dynamic-braking resistor introduced at balance, it is very easy to stop the setup motor consistently at the center of the vernier-balance segment.

Function of the Cam Cutout

The cam cutout is essentially a cam-limit switch having four separately adjustable cams. It is so geared that its cams revolve through one-half revolution while the main rheostat arm moves half a segment. The cutout is used in conjunction with the main rheostat in slowing down and stopping the screw-down motor in the following manner.

When the rheostat arm makes contact

Figure 6. Screw-down controller with contact-making ammeters



with the final or balance segment, the electronic-balance detector opens one of the two directional relays. This leaves the directional contactors under control of the cam cutout. The position of the cams at that instant is shown in Figure 3. Assuming the screw-down motor is driving the rolls in the down direction, the down-contactor cam has just closed its associated contacts to by-pass the contacts of the *DR* relay which are now open. The down contactors on the controller will thus be held closed until almost half a cam revolution later. During this half revolution of the cam cutout, two additional cams close their associated contacts in sequence to provide two steps of slow-down prior to opening the down contactors and setting the brake. A similar sequence occurs in the up direction, this time with the up-contactor cam and the same two slow-down cams. In the event of overtravel of the rolls, the motor will be "plugged" under the control of either the up or down cam according to the direction of motion.

For accurate final positioning, it is necessary to bring the motor to a predetermined speed before the cam cutout takes over control of stopping. The electronic-balance anticipator has proved valuable in improving the accuracy of stopping the screw-down motor by introducing slow-down circuits prior to reaching a balance on the main rheostat. Because of the need for higher speed in moving the rolls to the first pass position, it has been found worth-while to add a second balance anticipator so connected that it is effective only on the first pass.

Figure 4 shows a typical plugging-reversing controller for a single motor screw-down drive using a d-c series mill-type motor. Assuming two steps of anticipatory control the sequence of contactor operation during the slow-down and final stopping might be as follows.

The first and second anticipators close contactors *1SD* and *2SD* in sequence to strengthen the field giving two steps of slow down. At balance the electronic-balance detector opens the acceleration contactors *1A* and *2A* to reduce the voltage on the motor armature and further reduce the speed. The cam cutout takes over the control to insert two steps of armature shunt resistance by closing contactors *1AS* and *2AS*. Finally as the rheostat arm approaches the center of the balance segment, the directional cam opens the directional contactors and the slow-down contactors to permit the series brake to set.

Both the inertia and friction of the system may be changed when new rolls or guides are installed. It is a simple matter to adjust the controller for such changes by altering the position of the tap on resistor *R1* of the balance anticipators.

Figure 5 shows a 2-schedule panel of old design with a schedule selector switch and two 25-point 6-level stepping relays. The differential cutout assembly is shown mounted directly below the schedule panel. The use of a double schedule panel permits setting up two different rolling schedules. In changing from one to the other it is necessary only to move the schedule-selector switch. The double schedule panel has been found useful in those cases where the schedule is changed quite frequently, the change being made on one while operating the mill on the other.

The two groups of receptacles at the top of the panel are used in selecting the total number of passes after which the stepping relays return to pass number 1.

The main disadvantage to this form of schedule panel is the difficulty in following the cords in checking a preset schedule. It, however, has the advantage of occupying a small space. This is accomplished by limiting the number of receptacles on the assumption that it will not be necessary to use more than a few for any one increment in taking care of all the pass positions required in actual operation.

Figure 6 shows a typical factory-assembled screw-down motor controller and the associated relay panel for automatic preset operation. The two polarized contact-making ammeters are located at the top of the relay panel.

Figure 7 shows the new design of schedule panel for a single schedule. The larger panel with 30 increments is the main panel while the smaller is the vernier panel with 20 increments. It will be observed that the rolling schedule may be checked at a glance. The final pass selector is shown plugged in at pass number 15.

Figure 7. New schedule panel

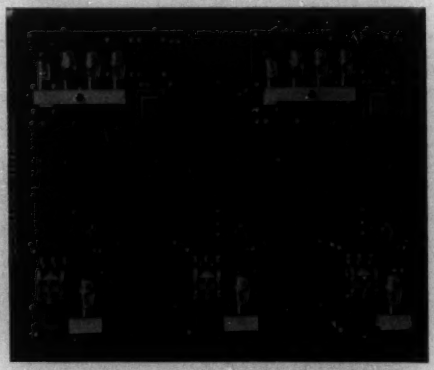


Figure 8. Electronic balance detectors and anticipators

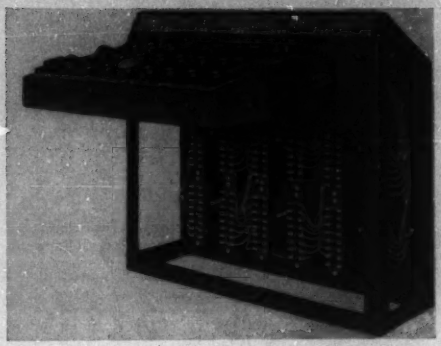


Figure 9. Operator's desk

The electronic-balance detectors and anticipators are shown in Figure 8. Two balance detectors, one for the main and the other for the vernier, replace the two contact-making ammeters shown in Figure 6. Three balance anticipators are shown, one for the set-up motor and two for the screw-down motor. All three anticipators are identical as are the two balance detectors. The adjusting potentiometers are shown below the slow-down relay on each anticipator.

The control desk of a plate mill screw-down controller is shown in Figure 9. In this mill both the vertical and horizontal rolls were controlled by automatic preset screw-down controllers while the front and rear side guards were automatically moved in and out under the control of short-travel limit switches. The six dials at the rear of the control desk are "Productimeters" which give the separation of both the edger and the horizontal rolls along with the position of the side guards. The Productimeters are driven by "selsyn" receivers governed by transmitters tied to appropriate parts of the mill.

Operation of the Screw-down Controller

As long as a-c power is on the synchronous-tie motors, the mill rolls and the differential cutout will be held in synchronism even though the rolls are moved under manual control beyond the range of the schedule panel. In the event of loss of a-c power the a-c electrical interlocking, which normally would stop the controller, can be by-passed to permit operation of the mill by manual control. In such an event it will be necessary to resynchronize the mill and the differential cutout before again rolling under automatic control.

A cold ingot or a cobble may make it necessary to depart from the preset rolling schedule. A separate master switch is provided for conventional manual operation. It is so connected in the circuit that when it is moved from the off point, it takes over complete control of the screw-down motor, and the automatic preset controller cannot operate unless the master switch is in the off position.

It may happen that after having operated the screw-down manually for a few passes, it is desirable to return to the preset schedule. In this case it is necessary only to return the master to the off point, step the ratchet relay forward to the required pass by a push-button provided for this purpose, and press the operating button. The rolls will then be moved to the preset position for that pass, and the rolling can be continued under preset control.

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No Discussion

Service Restoration With Automatic Air-Break Switches

KENNETH N. REARDON
MEMBER AIEE

AUTOMATIC pole top air switches for more than twenty years have played an important part in the operation of the 25,000-volt system of the West Penn Power Company. Nearly one hundred are now in service; they promptly, reliably and economically sectionalize lines, isolate faulted equipment, and transfer power loads from one source to another. No definite limit has been set for the size of loads controlled by automatic air switches; the largest transformer bank to date is of 3,000-kva capacity.

Control circuits have been kept simple and direct; operating procedure is such that, except for special cases, no close co-ordination of timing is required with associated automatic reclosing circuit breakers. Control schemes and the philosophy of their application are presented in this paper.

General

Automatic air-break switch installations may be classified into five general types descriptive of their functions; namely, by-pass, grounding, isolating, transfer and sectionalizing.

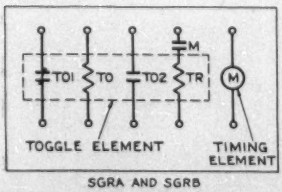

Figure 1. Operation lockout relay used in transfer and sectionalizing control

Type 1—By-Pass

This function is accomplished by a single air switch normally open and relayed to close when events make such action desirable. It is used to by-pass a station which has been relayed out of service because of trouble in the station itself. Occasionally in an area of heavy load concentration, a network must be strengthened by tying together automatically a pair of lines which have been separated by a substation bus fault.

Type 2—Grounding

This function finds perhaps its sole application in the protective relaying of a transformer bank tapped to a transmission line with automatic circuit breaker protection at the distant ends of the line. The grounding air switch is installed on the line side of the transformer bank; between it and the line there is usually installed an automatic opening air switch (especially if the transmission line has other loads connected to it). Protective relays, operated by a fault in the bank, trip open the local load side circuit breaker and close the line side grounding air switch. The line promptly trips at its distant ends and the transformer bank line side air switch opens under a no-load condition. The bank is thus isolated and the transmission line may again be placed in service.

Type 3—Isolating

The function of this single air-switch type is to isolate a faulted line or transformer bank. The isolation of a transformer bank has been described in the foregoing discussion of the grounding switch function. An application is found also in the automatic disconnection of a branch line from an important tie line. The air switch is normally closed and opens when the tie line is relayed out of service; usually the opening is delayed until at least one test has been made of the entire circuit. The disconnection of the branch may make it possible to restore the main line to service.

Type 4—Transfer

This application consists of two automatic air switches installed in close proximity. One is normally closed and one is normally open; a transformer load is connected to the bus between them. A source of power is obtainable through either of the air switches; neither source is considered "preferred" and the station will remain connected to the source from which it is receiving power until that source fails, whereupon it will transfer to the other source of power if that second source is available. If a permanent fault occurs on the bus between the automatic air switches, a transfer operation will take place and thus the bus will receive one test. After this one test both air switches will stand open until manual switching is performed. With both switches open and the fault thus isolated, all other sections of the line can be energized from their respective sources of power.

Type 5—Sectionalizing

The sectionalizing installation may consist of one switch strategically located at a selected point in a long stub feeder or in a tie line between two substations. It may take the form of two air switches located at widely separated points so as to break the circuit into three sections. The

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Kenneth N. Reardon is engineer of switchgear with the West Penn Power Company, Pittsburgh, Pa.

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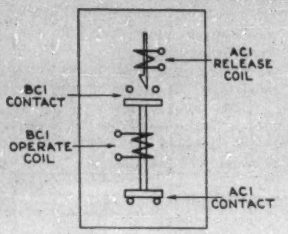

Figure 2 (left). Selector relay used in transfer control

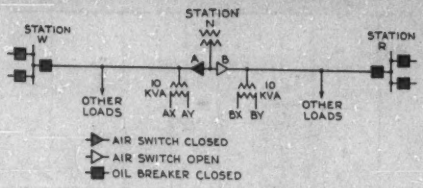

Figure 3. Transfer type station *N*

most common type of sectionalizing air switch application consists of two air switches installed in a line with a bus between them to which a transformer load is connected. A source of power is available from each end of the line. Both air switches are normally closed and both are relayed to open simultaneously when the through line is de-energized.

Power will always be restored to the substation bus from the first line section which is re-energized; the sections will be closed together if both become energized. Two of these automatic sectionalizing stations may be readily connected in series between two power sources with a third power source brought to the section of line between them. Reliable operation can be obtained even without that third source of power, although restoration of power to the mid-section will be less prompt.

Summary of Functions

The first three functions, by-pass, grounding and isolating are straightforward applications of automatic control which invite the attention of the application engineer. The remainder of this paper will be given over to the analysis and exposition of the other two functions transfer and sectionalizing because of the predominance of their use. They have proved to be effective and economical methods of service restoration.

Salient Features of Application

L. F. Hunt of the Southern California Edison Company has stated in his excellent paper[1] that:

"Simplicity is of paramount importance when designing and laying out protective relay circuits. These circuits are called upon so infrequently to operate that they should be more reliable than the apparatus or lines they protect. Many fancy conditions and requirements that are requested from the designers of circuits generally cause more fault tripping and thus become less reliable. When all the extra gadgets are left out of a protective system, the simple system operates so satisfactorily that the reasons for its good operation are generally forgotten."

Simplicity is commendable also in the application of functional relaying; the operation of automatic air switches lies in that category. Frequently the application engineer is burdened with control specifications which demand that the circuits cope with the unusual rather than the usual situation. Consideration of probabilities will result in circuit simplification without sacrifice of reliability.

The West Penn Power Company has built into the control of the automatic air switch that intelligence which would be expected of a well-trained substation operator. At the transfer station, the closed air switch is opened promptly when power fails, provided that the second source of power is available; the air switch to this second source is closed promptly and the substation bus again has power after a delay of about six seconds. At the sectionalizing station, both normally closed air switches are immediately opened upon loss of power and are closed promptly as soon as the individual lines become energized. Within ten seconds, the station bus again has power.

Lines are not tested prior to the action of the automatic air switches. Since line faults are frequently of a temporary nature, it is true that the air switches operate more often than necessary; there is certainly no harm in thus exercising the mechanism. When the line fault is of a permanent nature, there is definitely a gain in the speed of restoring service. If air switch action is delayed until one test is made, five seconds will be lost in restoring service to the transfer type of station and thirty seconds will be lost in restoring service to the sectionalizing type of substation. (Thirty seconds represents the time between the first and second tests.)

General West Penn Power Company Operating Conditions

William C. Bowen of West Penn Power Company and Edward Beck of Westinghouse Electric Corporation recently wrote[3] that:

"The 25-kv system of the West Penn Power Company is practically an overhead network. It feeds 374 step-down substations supplying customers and distribution loads and also serves directly a number of industrial customers. The system is located in regions where lightning storms are frequent and usually severe."

The 25-kv system is composed of lines ranging from 5 to 25 miles in length and is closely coupled with a strong 132-kv superimposed transmission system. The out-of-synchronism problem does not arise when sectionalizing air switches open and they can be reclosed without recourse

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Aa, Ab, Aa/b auxiliary switches of air switch *A* (Figure 3)

Ba, Bb, Ba/b auxiliary switches of air switch *B* (Figure 3)

a—Auxiliary switch—closed when air switch is closed

b—auxiliary switch—closed when air switch is open

a/b—auxiliary switch—closed only when air switch is in motion

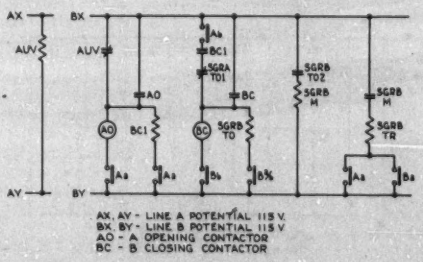


Figure 4. Control diagram; transfer of station *N* from air switch *A* to air switch *B*

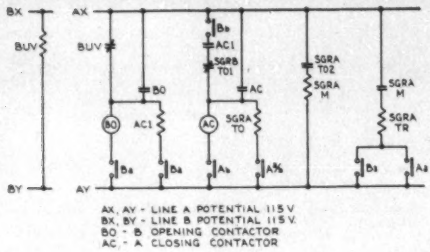


Figure 5 (left). Control diagram; transfer of station *N* from air switch *B* to air switch *A*

Auxiliary switches same as in Figure 4.

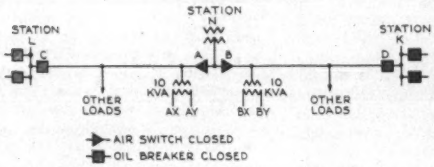


Figure 6 (below left). Sectionalizing type station *N*

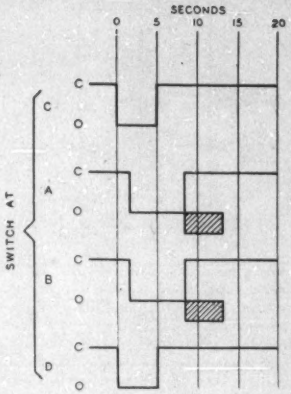


Figure 8 (right). Service restoration schedule for temporary fault anywhere on line *LK* of Figure 6

to synchronism check relays. The four components of an automatic air-break switch installation are (1) a means of detecting that a service abnormality exists; (2) a relay control scheme to operate the air switches according to a predetermined plan; (3) a source of power for operating the air switches; (4) the air-break switch itself.

Potential transformers or power transformers of 10-kva capacity are used to detect service abnormality. Absence of voltage and presence of voltage are the criteria which exercise control over both the transfer and the sectionalizing type of automatic air-break switch installation.

The relay sequence is simple and is composed of time delay over-voltage and under-voltage relays combined with an operation lock-out relay and a latch type selector relay.

A 48-volt storage battery supplies the source of power for operating the air switches of the sectionalizing type of installation; batteries of higher voltage are used if installed at the air-switch location for other service. A single-phase 10-kva transformer connected to each of the two lines provides the power source for the transfer type of installation.

The air-break switch itself is a standard production item and need not be discussed in this paper. Motor operating mechanisms are used by West Penn Power Company entirely (the grounding type of air switch is, however, spring operated).

It has been stated previously that a permanent fault on the bus of either the sectionalizing or of the transfer station will result in both air switches opening and remaining open until manual switching is performed. Figure 1 shows the 2-element operation lock-out relay by which this important feature is accomplished. One element is a toggle-type relay; the other element is a timing relay. Operating coil *TO* is energized briefly when an air switch is traveling from the open to the closed position; by instantaneous toggle action, contact *TO1* opens and contact *TO2* closes. Contact *TO1* is in the closing circuit of one of the air switches; the air switch cannot automatically close unless that contact is in the closed position. Contact *TO2* completes the circuit of the timing element which is supplied potential from the line controlled by the air switch which has caused the toggle element to operate. At the completion of the timing period a contact *M* is made which causes the toggle relay to reset provided that (in the sectionalizing type of application) the air switch which operated the toggle relay is closed, or provided that (in the transfer type of application) either, or both, of the air switches is closed. If the line becomes de-energized during the timing period, the timer will instantly reset and again start its full timing period when the line is energized; the line must remain continuously energized throughout the timing interval be-

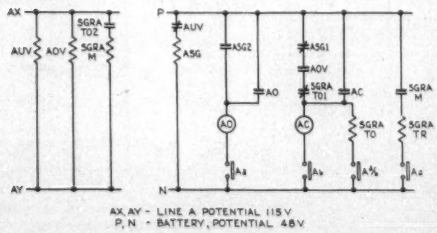


Figure 7. Control diagram for air switch *A* of Figure 6

Control diagram for air switch *B* is similar except apply letter *B* in place of *A* throughout. Auxiliary switches same as in Figure 4

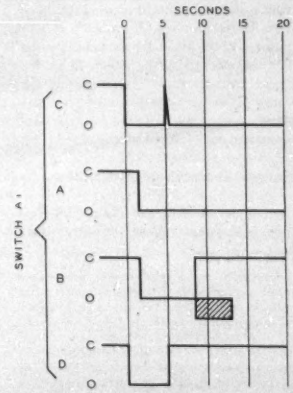


Figure 9. Service restoration schedule for permanent fault on section *LN* of Figure 6

fore a reset action can take place. Another relay that merits description is the electrically-operated electrically-reset latch type relay used as a selector in the transfer type of application. It is shown in Figure 2 and operates as follows:

When coil *BC1* is energized, contact *BC1* closes and latches closed. If coil *BC1* and coil *AC1* are simultaneously energized, contact *BC1* only will be closed because coil *BC1* controls the movement of the armature, whereas coil *AC1* merely unlatches the armature. The purpose of this relay is to prevent both air switches from closing if both lines become energized simultaneously after having been both de-energized by a total power interruption involving both circuits (see Figure 3). It also aids in the lock-out condition

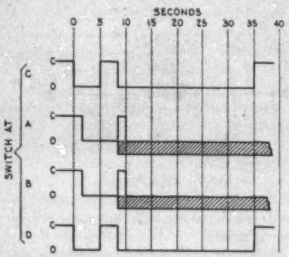


Figure 10. Service restoration schedule for permanent fault on bus of station N of Figure 6

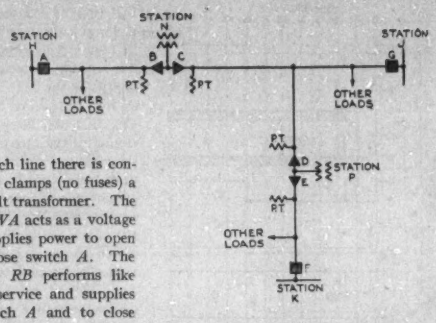
that takes place when a fault occurs on the bus between the air switches.

To simplify the control diagrams in this paper, the motor circuit of the air switches has been omitted; closing and opening contactors only have been shown, with an associated auxiliary switch that supplies a detail necessary for clear understanding of the circuit. A 4-pole double-throw manual-automatic transfer switch and two switchboard "Open-Close" control switches have likewise been omitted as being not essential to the description of automatic operation.

Operation of the Transfer Type of Automatic Air Switch

Figure 3 is an illustration of a 25-kv automatic transfer station. It is installed where the load is that of an industrial town or is one of a critical nature such as a gaseous mine ventilating fan or man hoist. Air switch *A* is shown closed, air switch *B* is shown open; both air switches are equipped with 115-volt a-c motor

Figure 11. A 3-source network with two sectionalizing type stations, *N* and *P*



mechanisms. To each line there is connected with live line clamps (no fuses) a 10-kva 25,000 125-volt transformer. The transformer on line *WA* acts as a voltage detector; it also supplies power to open switch *B* and to close switch *A*. The transformer on line *RB* performs like potential detection service and supplies power to open switch *A* and to close switch *B*. Stations *W* and *R* represent power sources to the lines *WA* and *RB*.

Loss of power supply from line *WA* results in the opening of air switch *A* and the closing of air switch *B;* within six seconds, power is thus restored to the substation bus. The substation then remains connected to line *RB* until an interruption occurs on that line whereupon it will transfer to line *WA*. The station always remains upon that line from which it is receiving power and always transfers to the other line when power is interrupted provided that the other line is energized.

When air switch *B* closes it energizes a lock-out relay which makes air switch *A* nonclosing until the lock-out relay resets. The lock-out relay will reset if after the closing of air switch *B*, line *RB* remains energized for five consecutive seconds. If line *RB* becomes de-energized as soon as air switch *B* closes, it is definite evidence that a fault exists upon the bus between air switches *A* and *B*. That being the case, as soon as line *WA* is re-energized, air switch *B* will open. Both air switches are now open and remain open. Line *WA* and *RB* can both be re-energized to supply power to other customers. Air switch *A* cannot reclose because the aforesaid lock-out relay has not reset. Air switch *B* cannot close again because when it tripped it opened its own closing circuit through contacts *BC1* of the selector relay (Figure 2). It should be noted that when a fault occurs on the bus between the air switches only one test is made. When line *WA* is the original source of power to the bus, the test is made from line *RB*. Line *WA* is not subjected to a second interruption nor is line *RB* after it has been re-energized.

Figure 4 is a schematic diagram of the control sequence which takes place when the substation is transferred from line *WA* to line *RB*. Events take place in the following sequence:

A fault occurs on line *WA* and the breaker at station *W* opens, de-energizing the line. Time delay undervoltage relay *AUV*, connected to the *WA* line potential transformer at *AX* and *AY* closes its contacts within a second and completes the circuit to the opening contactor *AO* of switch *A;* it also completes a circuit to coil *BC1* of the selector relay. Auxiliary contact *AO* maintains these coils energized until the air switch is nearly full open at which point the auxiliary switches *Aa* both open. Auxiliary switch *Ab* closes when the air switch is open and completes a circuit through contact *BC1* (which has just been closed) and contact *SGRA-TO1* to the closing contactor *BC* of air switch *B* and to the operating coil *TO* of lock-out relay *SGRB*. Auxiliary

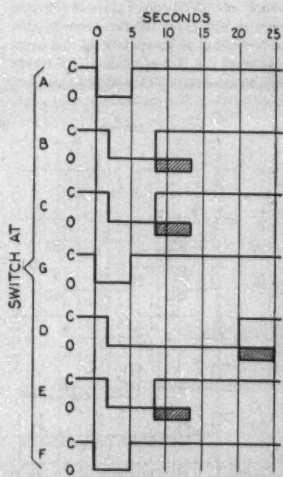


Figure 12. Service restoration schedule for temporary fault anywhere on network of Figure 11

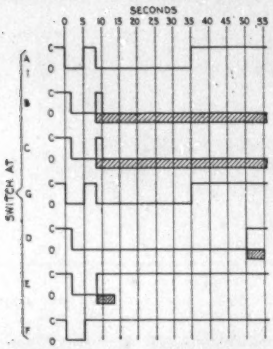


Figure 13. Service restoration schedule for permanent fault on bus of station *N* of Figure 11

contact *BC* keeps the contactor coil energized until air switch *B* is in the fully closed position. The station has transferred to the line *RB* and will remain there until automatically or manually returned to line *WA* at a subsequent time. While air switch *B* is closing, the operating coil *TO* of lock-out relay *SGRB* is energized briefly through auxiliary switch *Ba/b* which is closed only when the air switch is in motion. The toggle relay instantly opens contact *TO1* in the closing circuit of air switch *A* (see Figure 5) and closes contact *TO2* (of Figure 4) completing the circuit of *SGRB* motor *M*. If line *RB* remains energized for five seconds, this motor will close its contact *M* and energize reset coil *TR* of relay *SGRB* which will open contact *TO2* and close contact

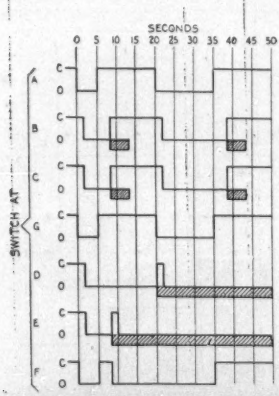


Figure 14. Service restoration schedule for permanent fault on bus of station *P* of Figure 11

TO1 thereby restoring the lock-out relay to normal operating condition.

If line *RB* does not remain energized for five seconds, as would be the case if the fault is on the bus of station *N* instead of on line *WA*, relay *SGRB* will not reset. Line *WA* next becomes energized by the closing of the breaker at station *W*; by referring to Figure No. 5 it can be seen that under-voltage relay *BUV* with its contacts closed, energizes coil *AC1* of the selector relay and the opening contactor *BO* of air switch *B* whereupon the air switch opens. It cannot close again automatically because contact *BC1* is open. Air switch *A* cannot close again automatically because contact *TO1* in relay *SGRB* is open.

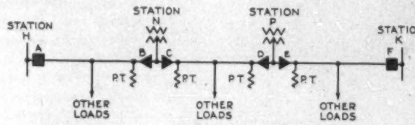
Both air switches are now open and neither can perform any further automatic action. An operator is dispatched to the station, clears the fault from the substation bus, and closes one of the two air switches to restore service to the substation. After five seconds, the lock-out relay will reset regardless of which air switch has been closed by the operator. At all of these stations a four pole double throw automatic-manual transfer switch is provided. The operator throws this switch to the manual position in order to operate the air switches electrically and restores it to the automatic position upon leaving the station in normal operating condition. Manual mechanical operation of the air switches is possible also if the need arises.

Operation of the Sectionalizing Type of Automatic Air Switch

Figure 6 is an illustration of a 25-kv automatic sectionalizing type installation. It is used where there is a load of a critical nature connected to a tie line between two substations. (The tie line aspect makes this installation different from the transfer type station of Figure 3 which has two independent sources of power.) Air switches *A* and *B* are both normally closed and both are equipped with 48-volt d-c motor mechanisms. A potential transformer (or a 10-kva 25,000 125-volt transformer) is connected on the line side of each air switch with live-line clamps (no fuses). The transformer supplies voltage to an over-voltage relay and an under-voltage relay. The air switches are operated by a 48-volt battery.

Both air switches open within three seconds time after line *LK* is de-energized because of a line fault. Breaker *C* at station *L* and breaker *D* at station *K* close automatically and test line sections *LA* and *KB*. The unfaulted section will remain energized and the corresponding automatic air switch at substation *N* will close within three seconds, restoring power to the bus of station *N*. If the other section of the line is subsequently re-energized, the second air switch at substation *N* will close and the entire line will be restored to normal. If the fault is permanent in nature, the second air switch will not close. Substation *N* will always obtain power from the line section which is energized first; the entire line will be restored to service if both sections prove to be good.

Figure 15. A 2-source line with two sectionalizing stations in series



If a fault occurs on the bus between the automatic air switches, both will open. Air switch *A* will close as soon as the section *LA* is energized and will again open when the circuit breaker at *L* again trips. Air switch *B* will likewise close and open. Both air switches will then remain open until they are closed by an operator. This lock-out is accomplished by the relay shown in Figure 1; after having been closed electrically, the air switch cannot at a subsequent time automatically close unless it has reset its lock-out relay by remaining closed for five seconds.

Figure 7 is the schematic control diagram for automatic air switch *A*; the control diagram for automatic air switch *B* is exactly the same. The control circuits of switch *A* are entirely independent of those for air switch *B*. In that respect this control scheme differs from the interlocked control of the air switches of the transfer type of installation (see Figures 3, 4 and 5). A description of the operating sequence which takes place when air switch *A* is called on to operate is applicable also to air switch *B*.

When line *LK* is de-energized, over-voltage relay *AOV* (Figure 7) opens its contacts *AOV* and under-voltage relay *AUV* closes its contacts *AUV*. These

two relays are independent time delay relays and in order to make certain there will not be a control conflict caused by contacts of both relays being simultaneously in the closed position, precedence is given to the under-voltage relay *AUV*. Its contacts *AUV* energize a 48-volt auxiliary relay *ASG* which opens contact *ASG1* in series with contact *AOV* and closes contact *ASG2* which completes the circuit to the opening contactor *AO* of air switch *A*. This contactor is kept energized by its auxiliary contact *AO* until the air switch travels to the full open position.

When line *LA* is re-energized, contact *AUV* opens; relay *ASG* is de-energized thereby opening contact *ASG2* and closing contact *ASG1*. A circuit is completed through over-voltage relay contact *AOV* and contact *TO1* of lock-out relay *SGRA* to the closing contactor *AC* of air switch *A*. This circuit is maintained through auxiliary contact *AC* and the air switch travels to the closed position. When the air switch is moving from the open to the closed position the operating coil *TO* of lock-out relay *SGRA* is briefly energized through auxiliary switch *Aa/b* which is closed only when the air switch is in motion; contact *TO1* opens and contact *TO2* closes completing a circuit to the motor *M* of the lock-out relay. If the line remains energized for five seconds, this motor will complete its travel, close contact *M* and energize reset coil *TR* of the lock-out relay provided that the air switch is still closed. If, however, the air switch again opens before the motor completes its five second timing period, as would happen if there were a fault on the substation bus, the reset coil will not be energized; contact *TO1* will remain open, and air switch *A* will not again close automatically. It will remain open until an operator closes it. A two-pole double-throw automatic manual transfer switch is provided to transfer the control to a standard control switch. Manual mechanical operation of the air switch is possible also if need arises.

Figures 8, 9 and 10 graphically show the time schedule of service restoration for the sectionalizing station of Figure 6. Circuit breakers *C* and *D* are set to reclose at 5-second and 30-second intervals. Air switches *B* and *C* are set to trip about one second after the lines are de-energized and to close about three seconds after lines have been again energized. The shaded portion indicates the period of time, five seconds or more, that the closing circuit is rendered nonoperative after a closing operation. Figure 8 shows that after a temporary fault, the two line sections are re-energized in five seconds and

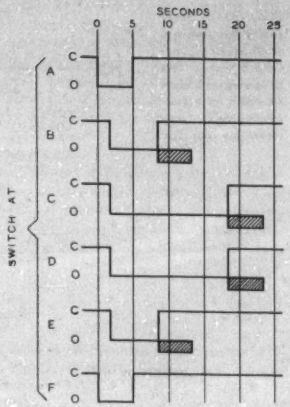


Figure 16. Service restoration schedule for temporary fault anywhere on line *HK* of Figure 15

station *N* in about nine seconds. Figure 9 shows that section *BD* is re-energized in five seconds and station *N* in nine seconds after the occurrence of a permanent fault on section *CA*. Figure 10 shows that both sections of the line are re-energized in 35 seconds after a fault on the bus of station *N*; the two open air switches *A* and *B* isolate station *N*.

Two Sectionalizing Stations With Three Power Sources

A network with three sources of power and with two automatic sectionalizing air switch stations is shown in Figure 11. Circuit breakers *A*, *F*, and *G* are set to reclose at 5-second and 30-second intervals. The four air switches, *B*, *C*, *D*, and *E* open one second after the lines are de-energized. Air switches *B*, *C*, and *E* close three seconds after their respective lines have been energized; air switch *D* closes 15 seconds after its line has been energized. Figures 12, 13, and 14 respectively show service restoration schedules for a temporary fault on the network, a permanent fault between air switches *B* and *C*, and a permanent fault between air switches *D* and *E*. The shaded portion indicates the period of time, five seconds or more, that closing circuits are rendered nonoperative after a closing operation. The closing time of fifteen seconds for air switch *D* is a requirement in order that the two good sections of line may be joined together after switching has taken place because of a permanent fault on the bus of either sta-

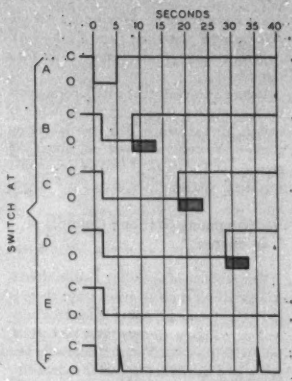


Figure 17. Service restoration schedule for permanent fault on section *FE* of Figure 15

tion *N* or station *P*. The time setting of air switch *D* must represent the sum of the closing time of air switch *C* plus its lock-out relay reset time plus approximately five seconds for safety factor.

Figure 12 shows that all sections of the line including stations *N* and *P* are re-energized within nine seconds after a temporary fault has occurred anywhere on the network. Airswitch *D* closes about twenty seconds after the fault occurs, and the entire network is again restored to normal operating condition.

Figure 13 reveals that with a permanent fault at station *N* between the air switches *B* and *C*, section *FE* including the bus of station *P* is re-energized in about nine seconds and all other sections

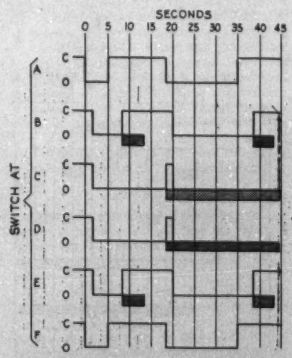


Figure 18. Service restoration schedule for permanent fault on section *CD* of Figure 15

again have power in 35 seconds; station N is isolated. Stations J and K are joined by the closing of air switch D in fifty seconds.

Figure 14 reveals that with a permanent fault at station P between air switches D and E all sections of the line including station N receive power in about 38 seconds and stations J and H are joined together; station P is isolated.

Sectionalizing Stations Connected in Series

The problem of operating two sectionalizing stations in series arises when there is not a third source of power to feed the mid-section. Figure 15 illustrates that situation with stations N and P connected in series in a line between power source stations H and K. Circuit breakers A and F are set to reclose at 5 and 30 second intervals. Air switches B, C, D, and E open about one second after their line sections are de-energized. Air switches B and E close about three seconds after their respective line sections have been energized. Air switch C closes 10 seconds after potential is restored to either section CD or to the bus of station N; air switch D closes 10 seconds after potential is restored to either section CD or to the bus of station P.

A potential transformer can be installed on the bus at stations N and P to determine their energized condition; more economically, the energized condition of station N can be detected by an electrical circuit combining the facts that the potential transformer on line AB is energized and that air switch B is closed. A similar recognition of station P bus potential can be obtained by using the potential transformer on line FE and the fact that the air switch E is closed. The control

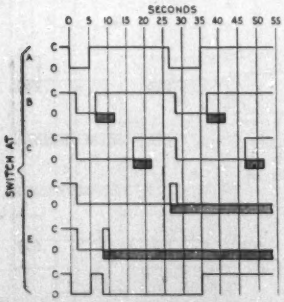
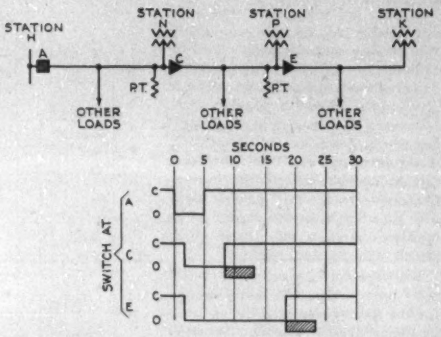


Figure 19. Service restoration schedule for permanent fault on bus of station P of Figure 15

Figure 20. A single source line with two sectionalizing stations in series; and its service restoration schedule for a temporary fault anywhere on the line



scheme of air switches C and D requires the addition of a simple voltage transfer relay; otherwise the control scheme of the four air switches is exactly the same as that (Figure 7) of other sectionalizing stations discussed in this paper.

The service restoration schedule shown in Figure 16 covers the case of a temporary fault anywhere between stations H and K. Power is restored in about nine seconds to station AB including station N and to section FE including station P; approximately 10 seconds later power will be restored to section CD and the entire line will be restored to normal operating condition. Figure 17 shows the schedule for a permanent fault on line section FE; power is restored to section AB and station N within nine seconds; section CD is energized 10 seconds later and station P ten seconds still later. Section EF is isolated. A similar train of events will take place for a permanent fault on line section AB.

Figure 18 shows the restoration schedule for a permanent fault on section CD and reveals that power is restored to sections AB and FE in 35 seconds, and to stations N and P about three seconds later.

Figure 19 shows the restoration schedule for a permanent fault on the bus of station P. Sections AB and FE are re-energized in 35 seconds, station N in about 38 seconds, and section CD in about 48 seconds. A schedule for a fault on the bus of station N would reveal similar results.

Figure 15 shows two critical stations, N and P, in the line between power source stations H and K. Even without those critical stations, it is occasionally desirable to break a line into two or three sections so that a permanent fault on one section may be isolated and service restored to the other sections. The line diagram then becomes that of Figure 15 with the omission of stations N and P and of air switches C and D. Circuit breakers A and F and air switches B and E operate exactly as described previously in the discussion of Figure 15. Sections AB and FE are re-energized five seconds after a temporary fault and section BE is energized about three seconds later. Permanent faults on line sections are isolated promptly and service is restored to the good sections.

The line between stations H and K can be divided into two sections by a single automatic air switch so wired that it will close when both line sections are energized. A potential transformer on both sides of the air switch is required.

Sectionalizing Air Switches in a Single Source Feeder

Figure 20 illustrates a line HK with a power supply from station H only. The line is long and heavily loaded; there are valid operating reasons for sectionalizing the line into three parts upon occurrence of a fault and thereafter restoring power to as much of the line as possible. Circuit breaker A has a reclosing setting of 5 and 30 seconds. Air switch C and E both open when the line is de-energized; air switch C closes when section AC is energized and air switch E closes when section CE is energized. The air switch control scheme is exactly as has hitherto been described for the sectionalizing type (see Figure 7).

Figure 20 shows also the service restoration schedule for a temporary fault occurring anywhere on the feeder; the entire feeder is re-energized in about 18 seconds. With a permanent fault on section EK the other two sections will be re-energized in about 38 seconds. With a permanent fault on section CE

power will be restored to section AC in 35 seconds.

Conclusions

The types of automatic air break switch application described in detail in this paper together with other types of which brief mention is made can all be effectively used to mitigate the problem of service restoration. The automatic air switch not only isolates faulted equipment, preventing further damage, but also isolates faulted line sections, permitting other sections to be re-energized. By their automatic action they designate within definite limits the location of faults and thus assist in the task of directing repair crews to the affected area. Combinations of sectionalizing and transfer types on the 25-kv network of the West Penn Power Company have been very successful.

Because of their satisfactory performance over a period of many years, West Penn Power Company is continuing to make automatic air switch installations where unusual or critical loads exist, some of which might otherwise require the installation of oil circuit breakers. Automatic air break switches have their limitations of application because of their slow speed of operation and because of their inability to reliably open currents of fault magnitude; they cannot take the place of circuit breakers when those operating characteristics are essential.

For service restoration purpose the automatic air switch is an outstanding device accessory to and complementary to the oil circuit breaker.

Discussion

R. S. Smithley (Duquesne Light Company, Pittsburgh, Pa.): This paper shows what can be done toward improving service to loads served at transmission voltages, that do not justify automatic service. The schemes developed and used by Reardon restore service in a very short time. Because of the desire for restoration of service in a short time (approximately 10 seconds), the schemes require the use of high tension potential or station service transformers on each line, fast operation of air break switches and possible closing of air break switches against faults. The frequent operation of air break switches and the closing of these switches against faults apparently does not result in need for excessive maintenance of the switches.

The Duquesne Light Company has 24 service-restoring installations for nonautomatic stations on the 23-kv system. Several more installations will be made in connection with unit substations. The outstanding differences between the general scheme employed by the Duquesne Light Company and that employed by Reardon are basically five.

1. Use of 3-phase bus potentials instead of single-phase line potentials.

The 3-phase bus potentials generally are obtained from low-tension bus potential transformers.

2. Attempt to re-energize the line before sectionalizing.

Avoids necessity of operating air-break switches on temporary faults.

3. Timing is done at no-voltage and a battery source is used for operating the air-break switches.

Operating the switches while the station is dead necessitates a battery because there are no high tension line potential transformers.

4. No provision is made for bus faults at the sectionalizing station.

May cause outages to other nonautomatic customers on the line.

5. Longer time may be required to restore service.

The economic advantage of using existing potential transformers instead of high tension potential transformers on each line results in a lower cost installation, at the expense of increased service restoration times which may be of the order of one minute. The performance of these schemes has been entirely satisfactory.

Edward W. Kimbark (Northwestern University, Evanston, Ill.): The service-restoration schemes described in the paper entail 8-second interruptions of service to tapped loads upon the occurrence even of temporary faults. These interruptions could be made much shorter by providing the circuit breakers at one or more points of feed with high-speed initial reclosure. If this were done, service would be restored in one-third to one-half second after occurrence of a temporary fault and with no operation of the air-break switches. Permanent faults could be isolated selectively in accordance with the schedules described in the paper when the high-speed reclosure did not succeed.

The addition of high-speed reclosure as just suggested would, of course, necessitate larger circuit breakers because of the more severe duty cycle and also, on lines fed from two or more points, would require some form of pilot relaying. The additional cost of circuit breakers and relays might be warranted on some lines supplying power to important loads, especially on lines so constructed that the majority of faults are temporary.

Kenneth N. Reardon: The discussions from the floor following the presentation of my paper and the many conversations in which I subsequently took part definitely indicate that a great interest exists among operating engineers in the use of the automatic air switch as a service restoring device. The field of application is apparently widespread geographically; satisfaction with results obtained and active plans for continued application characterized the comments. O. L. Giersh of the Duke Power Company revealed in his remarks during the meeting that the automatic air switch by performance has earned for itself an important position in his relaying and service restoration program. Several hundred are automatically controlled so as to act as back-up devices for relayed circuits; mention was made of their use in place of fuses for the protection of transformer banks. More information, as might be given in an "experience paper," would be of interest.

Edward W. Kimbark, professor of Northwestern University, pointed out the possibility of reducing the duration of interruptions to an even greater extent than described in my paper by the expedient of using an initial high-speed reclosure of circuit breakers at one or more points of feed. Recognition was given by him to the fact that a prerequisite for the success of such immediate reclosing might in many cases be the installation of pilot wire relaying. True, instantaneous reclosing could reduce the duration of interruption in the case of a temporary fault to the attainable minimum of time. It is a practice most applicable, in our opinion, when the automatic air switch installation is of the transfer type and in many instances is used on the West Penn Power Company lines. Mention to that effect was not made in my paper which was written to present the general picture and the various types of application. Specific installations with their requirements for pertinent timing sequences were left to the ingenuity of the individual engineer.

Instantaneous reclosing of circuit breakers is less feasible at the two or three ends of lines of which the sectionalizing type of air switch is a part, but again may well be considered for a specific installation. In our case, the opening of circuit breakers on such lines is not simultaneous at all points, but is more or less sequential. Our experience has shown that it is better to automatically sectionalize the line and then with surety re-energize it section-by-section thereby giving definite possibility of restoring service to the bus between the automatic sectionalizing switches. Pilot relaying is not applicable in our case because numerous transformer banks with fuse protection and customers with loads of considerable magnitude are connected between the source substations. Unbalanced load currents would flow in pilot circuits under normal conditions were they to be installed and lines might be tripped unnecessarily when faults in connected transformer banks call for the blowing of fuses.

R. S. Smithley of the Duquesne Light Company presented interesting data on the use of automatic air switches by the Duquesne Light Company and described various important operating functions which have been achieved.

G. E. Heberlein of the Railway and Industrial Engineering Company commented upon the use of automatic air switches in general and asked for an expression of our experience of connecting potential and power transformers directly to the lines without fuses. We have had excellent operating

experience in that respect and do not consider fuses to be necessary. In answer to his question, we stated also that counters are used on the air switch mechanisms some of which operate mechanically, the majority electrically. No effort is made to transmit information to a dispatching location that the air switch has operated or that the air switch is in a contrary-to-normal position. The use of current transformers or potential transformers, wherewith to obtain directional relaying, was discussed in response to Heberlein's question and we pointed out that we have strived to obtain automatic air switch operation without resource to the complications of directional selectivity, and have accomplished that end very successfully.

L. F. Ferri of the Ohio Public Service Company presented an interesting discussion of automatic air switches with timing co-ordinated with reclosing circuit breakers and repeater fuses. His discussion was informative and indeed appreciated.

I suggest that the subject of automatic air switches is one that can well bear further discussion by AIEE groups in the various geographical locations and that an exchange of ideas and operating practices will be profitable.

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1. Simplicity in Protective Relaying, **Lloyd F. Hunt.** AIEE Transactions, volume 65, 1946, pages 995—9.

2. Lightning Investigation on the West Penn Power System, **William C. Bowen, Edward Beck.** AIEE Transactions, volume 66, 1947, pages 831–7.

Relaying Difficulties Disclosed by Staged-Fault Tests

W. A. MORGAN
MEMBER AIEE

BYRON EVANS
MEMBER AIEE

HIGH-VOLTAGE staged-fault tests previously have been described before the AIEE.[1] However, unusual data and experience obtained from staged-fault tests by the Bureau of Reclamation, Denver, Colo., on a power system in Arizona may be of help to engineers confronted with similar problems. Some of the difficulties encountered indicate the need for reviewing the design of some presently accepted types of modern equipment. The occasion for the tests was the installation of long-line-type directional-distance-carrier relaying and 20-cycle reclosing circuit breakers on the two 161-kv 140-mile Parker–Phoenix transmission lines.

As a result of the tests and corrective measures taken, the system operated through the 1947 summer lightning season with correct carrier-current relay and 20-cycle reclosing circuit-breaker operations for the two lightning flashovers that occurred. However, oscillographic records were necessary to reveal the unforeseen difficulties. If the protective equipment on the two 161-kv lines had been put into service without these tests, it is probable that the first natural line fault would have caused a severe interruption to service, and data necessary to determine the cause of the interruption would not have been available. Two series of staged-fault tests were made. The first tests of February 1–2, 1947, disclosed a number of relay troubles. The second tests of February 15–16, 1947, were made after these troubles had been either corrected or remedied by temporary expedients.

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W. A. MORGAN is group head of, and BYRON EVANS is an engineer in, the power system technical group of the branch of design and construction, Bureau of Reclamation, Denver, Colo.

The authors wish to acknowledge the co-operation received from engineers of the Southern California Edison Company, Metropolitan Water District of Southern California, Central Arizona Light and Power Company, Salt River Valley Water Users Association, Tucson Gas Electric Light and Power Company, Imperial Irrigation District, United States Indian Service, and other interconnected utilities.

Description of the Power System

The location of the two Parker–Phoenix 161-kv lines with respect to the interconnected system that existed at the time of the tests is shown in Figure 1. The Parker power plant was placed in operation in December, 1942. Its output was soon absorbed by the Phoenix, Yuma, Coolidge, and Tucson loads, and the initial, single 161-kv Parker–Phoenix line proved to be inadequate. The second 161-kv Parker–Phoenix line was put into service during 1946. Both 161-kv Parker–Phoenix lines have overhead ground-wires. The critical need for power in the central Arizona area has required a larger capacity 230/161-kv interconnection at Parker power plant to make more power available from Hoover power plant. Davis power plant, now under construction, will connect into this power system and augment the present available power supply by 225,000 kw. The importance of the relay and 20-cycle reclosing circuit-breaker performance on this rapidly growing power system is appreciated.

Calculating Board Stability Studies

Calculating board studies were made in July 1944, after it was apparent that the single 161-kv Parker–Phoenix transmission line was inadequate. Stability studies and system impedance measurements demonstrated the need for carrier-current relaying and 20-cycle reclosing circuit breakers, and determined the relay characteristics required to prevent false relay operation on the low impedances which accompany power swings. Figure 2*B* illustrates one of the power-swing studies and shows that a load of 103 megawatts, which is in excess of the capacity of one 161-kv Parker–Phoenix line, may be delivered to Phoenix, and stability will be maintained when a fault occurs on the other line, provided 20-cycle reclosing circuit breakers are used. In Figure 2*B* it will be noted that the relative angles of the machine internal voltages with respect to the Parker machines as a reference are plotted since the criterion of stability is determined by the swing of the machines with respect to each other. This method of representation is believed to be more direct and easier to interpret than the usual method of plotting the actual angular change of all the machines. Figure 2*C* shows how the impedance at the Parker Terminal, corresponding to the power swing on the unfaulted line, is reduced to values which would cause undesirable tripping of the unfaulted line if conventional distance relays were used. The 0.35 second point *P* of Figure 2*C* has

Table I. Staged Fault Tests

Performed on Parker-Phoenix Two 161-Kv, 140-Mile Transmission Lines

Test No.	Type of Fault	Fault Location
Initial tests, February 1–2, 1947		
1	Line-to-ground	Line 2 at Parker
2	Line-to-ground	Line 2 at Phoenix
3	Line-to-ground	Line 2 at Parker
4	Line-to-ground	Line 2 at Parker
5	3-phase	Line 2 at Parker
Check tests, February 15–16, 1947		
1	Line-to-ground	Line 1 at Phoenix
2	Line-to-ground	Line 1 at Parker
3	Line-to-line	Line 1 at Phoenix
4	Line-to-line	Line 1 at Parker
5	Line-to-ground	Line 2 at Phoenix
6	Line-to-ground	Line 2 at Parker
7	Line-to-line	Line 2 at Phoenix
8	Line-to-line	Line 2 at Parker
9	Double line-to-ground	Line 2 at Phoenix
10	Double line-to-ground	Line 2 at Parker

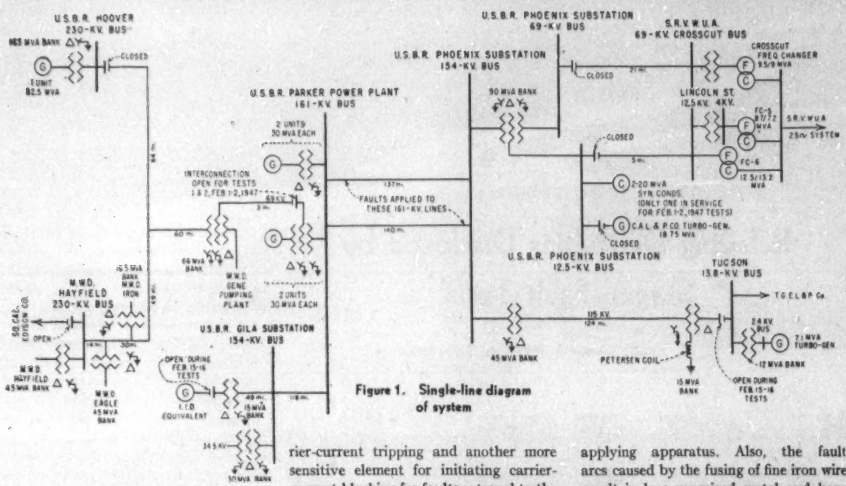


Figure 1. Single-line diagram of system

been plotted on Figure 3 to show that undesirable relay operation is prevented by the relays now installed on these lines.

The Parker–Phoenix system would become unstable if the circuit breakers on the faulted line were not reclosed at about 0.35 second. This is indicated by Figure 2*B* which shows the relative angles of the machines increasing rapidly before 0.35 second. The dashed line plotted on Figure 2*C* shows that the impedance seen by line number 2 relays, without rapid reclosure of line number 1 circuit breakers, would be within the tripping zone of the special relays now in service. This is satisfactory since the system would be unstable without rapid reclosing, and it would be desirable to separate by tripping the remaining 161-kv line.

Long-Line-Type Directional-Distance-Carrier Relaying and 20-Cycle Reclosing Circuit Breakers

The characteristics of the phase relays with the settings used at the Parker end are shown in Figure 3. Calculating board studies indicated that a restricted impedance area characteristic was required to prevent tripping during power system swings. These relay impedance requirements were met by using long-line-type directional-distance relays for fault detection, and blinder relays for preventing tripping on some of the power swing impedances. The carrier-current ground relays were the instantaneous directional-type with one element for initiating carrier-current tripping and another more sensitive element for initiating carrier-current blocking for faults external to the protected line. The ground backup protection is provided by directionally-controlled, time-over-current relays.

The 20-cycle reclosing circuit breakers had been installed on line 2 only, at the time of the staged fault tests. These circuit breakers are rated 5 cycles interrupting time and each pole has a resistor of about 1,300-ohms connected across each of the two interrupter main contacts. The operating mechanisms are the pneumatic-type. The 20-cycle reclosing circuit breakers for line 1 had not been received at the time of these tests and the existing oil circuit breakers were used. These circuit breakers are rated 5 cycles interrupting time, with a mechanical-type operating mechanism which cannot be adapted to 20-cycle reclosing service. There are no resistors across the main contacts of these circuit breakers.

Method of Applying Faults

The method of applying the 161-kv faults is illustrated by Figure 4. This method has been described in detail in a previous article.[1] It is sufficient to say that no difficulty was experienced with this method during the Parker–Phoenix tests. The arrangement of weights and pulleys provided a fast, convenient method for renewing the iron wire after each test. Oscillograph connection changes between tests required more time than did the renewal of the iron wire. Iron wire was preferred to ordinary fuse wire because it has sufficient tensile strength to withstand the loads imposed by the fault-applying apparatus. Also, the fault arcs caused by the fusing of fine iron wire result in less vaporized metal and have high-resistance characteristics similar to arcs caused by lightning flashovers. The faults were actually applied to the transfer busses which were connected to appear electrically as part of the line.

Description of Tests

The tests were scheduled during the light-load period over week ends when the load demand could be reduced to about 40 megawatts. With this power transfer, it was hoped to reduce the possibility of an interruption to service at Phoenix in case the 20-cycle reclosing circuit breakers failed to reclose, since it was thought that the unfaulted line could carry this load despite the power swing which would result with the fault. The schedule of the tests is shown in Table I.

During the tests a 12-element oscillograph was operated at Phoenix substation, a 9-element oscillograph was operated at Parker power plant, and a 7-element oscillograph was operated at Gene pumping plant. Oscillograms for the Gene oscillograph are not illustrated; however, these Gene oscillograms revealed that the slow relay operations in the February 1–2 tests caused power swings between Hoover and Parker of up to 40 megawatts, while these power swings during the February 15–16 tests, with faster relaying times, were only about half as great. Also, an instantaneous-type ground relay at Gene pumping plant operated during one of the ground faults on the 161-kv Parker–Phoenix line 1 at Parker because of the prolonged restriking time of the older-

type circuit breakers on that line. Similar faults on line 2 with the modern circuit breakers did not cause ground-relay action at Gene pumping plant. The oscillograms from the Parker and Phoenix oscillographs were of inestimable value in overcoming some of the difficulties, the principal ones of which are described in the following paragraphs.

False Operation of Carrier-Current Ground-Relay Elements on Line-Charging Transient Currents

Residual-transient current in the current transformers of the lines and of the transformer-bank neutrals when the 161-kv lines were energized by closing the line oil circuit-breakers is shown in Figure 5. The frequency of the transient current for these 161-kv lines is about 240 cycles. Although the duration of this transient is short, the magnitude is sufficient to operate the carrier-current ground-trip *GD2* elements when the relays are given the low settings required on this installation. It is believed that the frequency of this transient corresponds to the resonant frequency of the line capacitance and transformer bank reactance. The magnitude depends on the time of the cycle that the circuit-breaker main contacts close to energize the line. This same phenomenon has occurred on the 230-kv lines at Grand Coulee power plant even though the three poles of the circuit breaker had been carefully adjusted to assure the simultaneous mechanical closure of the main contacts of all three phases. It is believed that the line phases are energized at different instants due to the breaker-main contacts being bridged by arcs before they close mechanically.

The 161-kv lines were energized a number of times from the Parker end and it was found that the *GD2* relay on line 1 would operate occasionally with a setting of 1.4 amperes, whereas the ground relay on line 2 was less vulnerable. The settings of the *GD2* relays were left at 1.5 amperes for line 1 and 1.4 amperes for line 2. It is desirable to set the relays at 0.7 ampere to assure fast positive operation for faults during minimum system operating conditions. False tripping by these relays could be prevented by the use

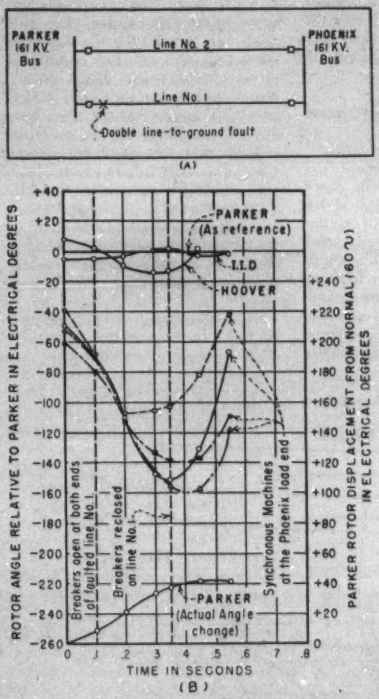
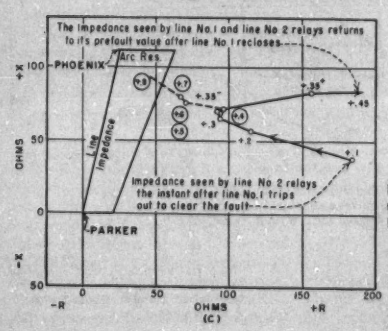


Figure 2. Effects of double line-to-ground fault at Parker determined by calculating board study

(a) Location of fault, (b) power-swing curves of the principle synchronous machines on the system, (c) impedances seen by phase relays on line 2 at Parker for fault 2a and time instants corresponding to those of 2b



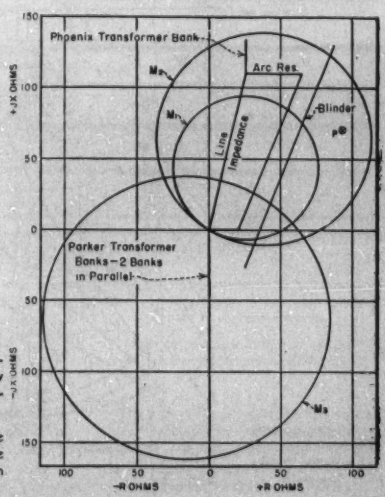


Figure 3. Characteristics of 161-kv line relays for phase-fault protection

Point *P* is the +0.35-second time point shown on Figure 2c

Table II. Reclosing Time Required to Prevent Re-establishment of Fault Arc

Test	Type of Fault	Time Interval Circuit Breaker Main Contacts Were Open	Was Fault Arc Re-established When Circuit Breaker Reclosed?
No. 2, Feb. 1–2	Phase-to-ground	8.4	No
No. 5, Feb. 1–2	3-phase	8.2	Yes. The fault restruck between *B* and *C* phases
No. 7, Feb. 15–16	Phase-to-phase	11.2	No

of a time-delay relay, but this is considered undesirable. A high-frequency filter improvised from capacitors and reactance coils available at the project was used to by-pass this line-charging transient-current from the relay-operating coil. Tests with this improvised filter circuit demonstrated some benefit and indicated that a correctly designed filter would prevent false operation of these relays, and the manufacturer has been requested to design and build filters for this installation.

The line-charging tests indicated that the interrupter-unit resistors used in the line 2 oil circuit breakers are somewhat effective in reducing the magnitude of the line-charging residual transient-current, because this current was larger on line 1 where the circuit breakers are not equipped with these resistors. This is illustrated by Figure 5. However, Figure 8 illustrates that the interrupter resistors in the circuit breakers do not obviate the need for filters in the carrier-ground relay circuits to prevent their false operation in low settings.

Contact Bounce of Carrier-Ground Relay Element

The carrier-ground relay element *GD1* starts carrier when its normally closed contact opens. Tests 1 and 2 of the February 1–2 tests demonstrated that this contact would intermittently start carrier and cause the carrier alarm bell to ring in the control room, immediately following the clearing of a line fault which had produced relay torque to hold this contact in the normally closed position. This is shown in Figure 6 where the complete oscillograph trace for line 1 carrier signal shows that carrier is periodically started and stopped about 13 times over a period of 51 cycles. This bouncing or vibration could not be duplicated in the laboratory, but since it had occurred elsewhere before, it was thought to be caused by the sudden reduction in contact pressure under certain current conditions resulting when the relay torque produced by the current element goes to zero when the fault is cleared. The ratio of operating and restraining-coil currents may be a factor, but this was not investigated in the laboratory. The relay contacts are of the new non-bounce design which is quite effective for preventing bounce when the contacts "make" under relay torque. To correct the bounce of the normally closed contacts when closing torque is reduced to zero, the stationary contact assembly was modified by removing the metal disk located directly back of the stationary contact, and by putting a spacer behind the ball to hold it directly against the stationary contact so this contact is allowed only about 1/64-inch movement.

The effectiveness of this modifica-

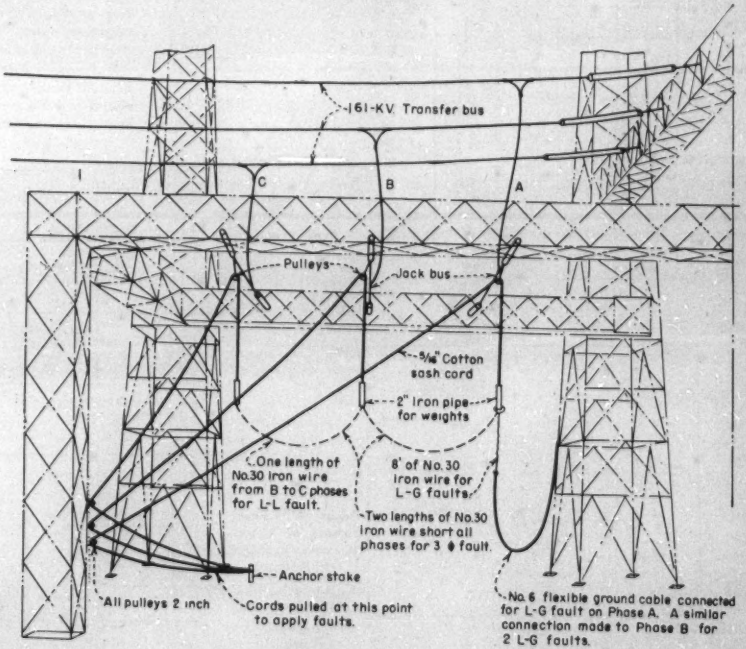


Figure 4. Method of initiating 161-kv line faults

tion is demonstrated by comparing the oscillograph trace for line 1 carrier signal in Figure 6 with Figures 8 and 11.

Incorrect Current Transformer Connections

The polarity of the connections of the directional-ground relay-polarizing current transformers in the main transformer bank neutrals at Parker power plant was assumed to be correct prior to the February 1–2 staged fault tests. The plant had been in operation about four years and the single 161-kv Parker–Phoenix line had relayed correctly so far as was known; however, the February 1–2 staged fault tests revealed that the polarizing current was nearly zero. See oscillograph trace, *In*, of Figure 6. A thorough check revealed that two of the four 161-kv neutral-current transformers were connected for reverse polarity, and if it were not for the difference in impedances of two-winding and three-winding transformer banks, and the small polarizing current supplied by the 69-kv neutral-current transformers, there should have been zero polarizing current. The explanation for this wrong connection probably is that Parker power plant was completed during the war when there was an urgent need for electric power and a shortage of manpower, and the connection of the two current transformers was left to an electrician to connect the same as the first ones had been connected. This had not been detected previously since the induction-type, directionally-controlled, ground relays had functioned several times before. The long operating time had not been measured previously with an oscillograph. This incorrect connection of the neutral-current transformers was responsible for the only interruption to service at Phoenix during either the February 1–2, or the February 15–16 tests. The fourth test of the first series caused tripping of both 161-kv lines in a slow time by the backup ground relays and reclosing of the one line was unsuccessful.

Incorrect Carrier Control Circuit

The staged-fault tests revealed a defect in the carrier-control relay circuit which was not apparent in checking through the drawings or circuits prior to the staged fault tests. With the incorrect circuit, carrier-blocking was allowed to start on the faulted line at the nearest terminal as soon as the oil circuit-breaker tripping relay was energized. This blocked tripping at the opposite end of the line until the circuit breaker at the faulted terminal had opened. This required that the relays at the far terminal had to operate before the auxiliary tripping relay of the circuit breaker at the faulted terminal was energized, otherwise sequential tripping would result. The circuit was corrected by relocating a circuit-breaker auxiliary, *a*, switch contact which was already in use in the relay-control circuit. This change did not require any new circuit wiring. The incorrect starting of carrier-blocking may be seen in Figures 6 and 7 of the February 1–2 tests. Correct carrier-control is shown by Figures 8 and 11.

The pips that occur in the carrier-current traces for line 2 in Figures 8 and 11 are not due to incorrect carrier-relay action or contact bouncing. This fact was established during one of the tests by connecting a jumper around all the relay contacts that would start carrier when their contacts were opened. This test showed that the pips in the carrier-current trace occurred as before, at the time of the fault, clearing of the fault, and at reclosing.

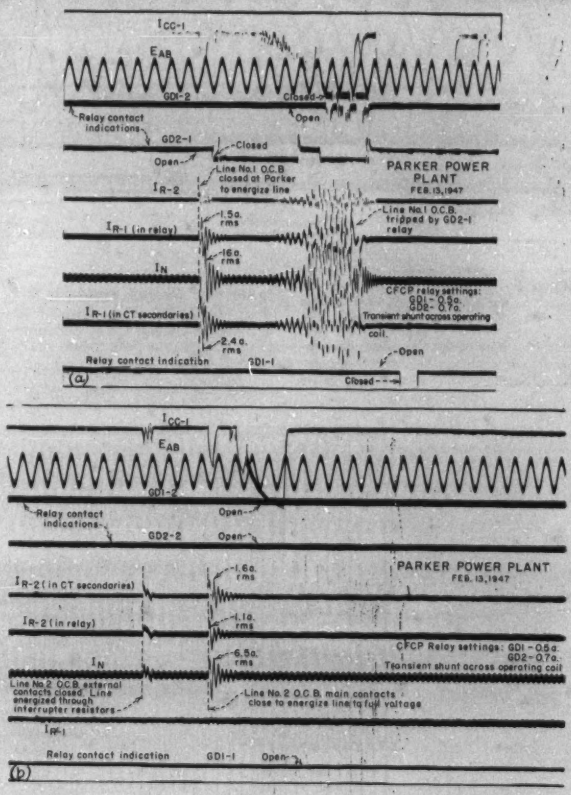


Figure 5. Residual-transient currents produced when 161-kv circuit-breakers were closed to energize the lines

(*a*) Maximum transient recorded on line 1; (*b*) maximum transient recorded on line 2

The pips that occurred in the carrier trace, therefore, are probably due to extraneous frequencies which accompanied the line disturbances and were within the range of the carrier-frequency channel.

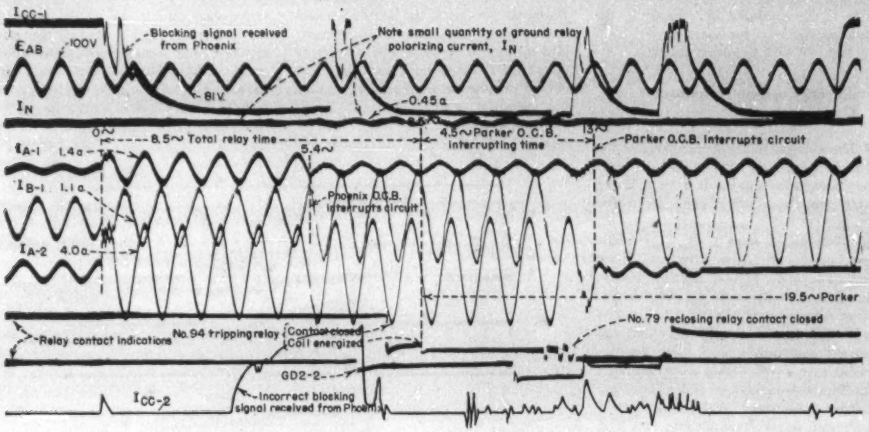


Figure 6. Oscillogram from Parker oscillograph for test 2, February 1–2, 1947. Phase A-to-ground fault on line 2 at Phoenix

Note evidence of contact bounce of carrier-ground *GD-1* relay, small ground-relay polarizing current, and incorrect carrier-blocking signal on line 2

Inadequate Backup Protection with the Long-Line-Type Directional-Distance Carrier Relay Scheme

In the conventional long-line-type directional-distance carrier relaying scheme, the third element *M3* is set for fast operation in the direction to start carrier-blocking, which is the opposite direction for which the first and second elements *M1* and *M2* are set to operate.[3] At a given terminal, *M3* does not provide

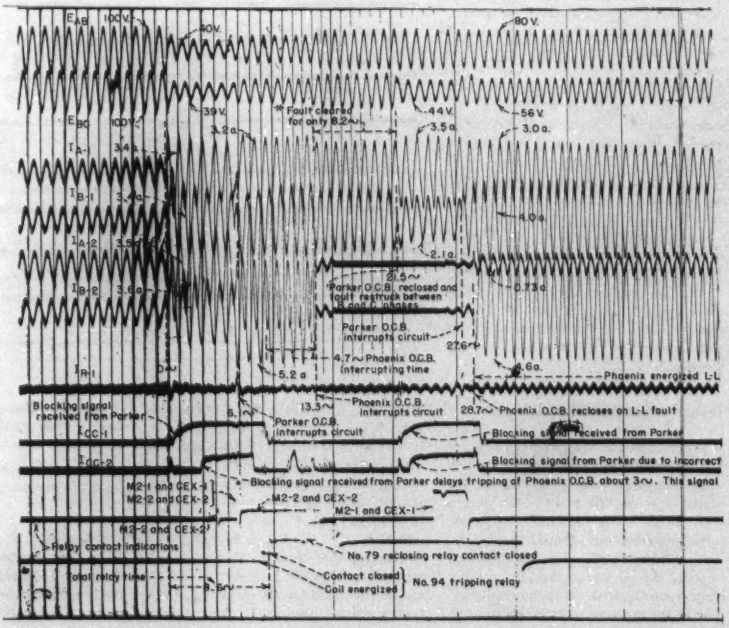
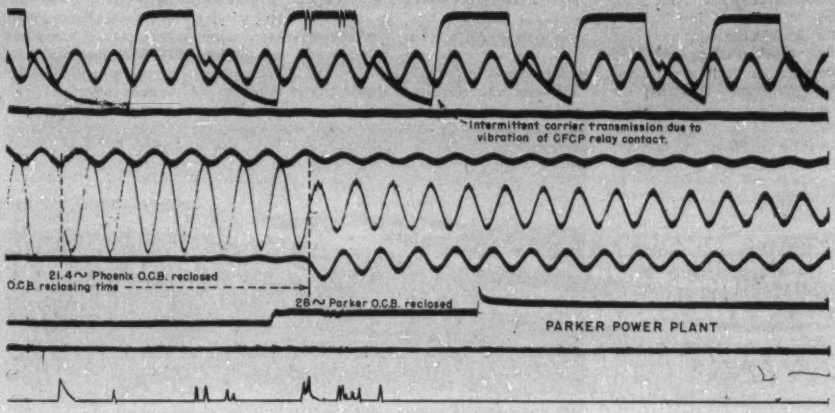
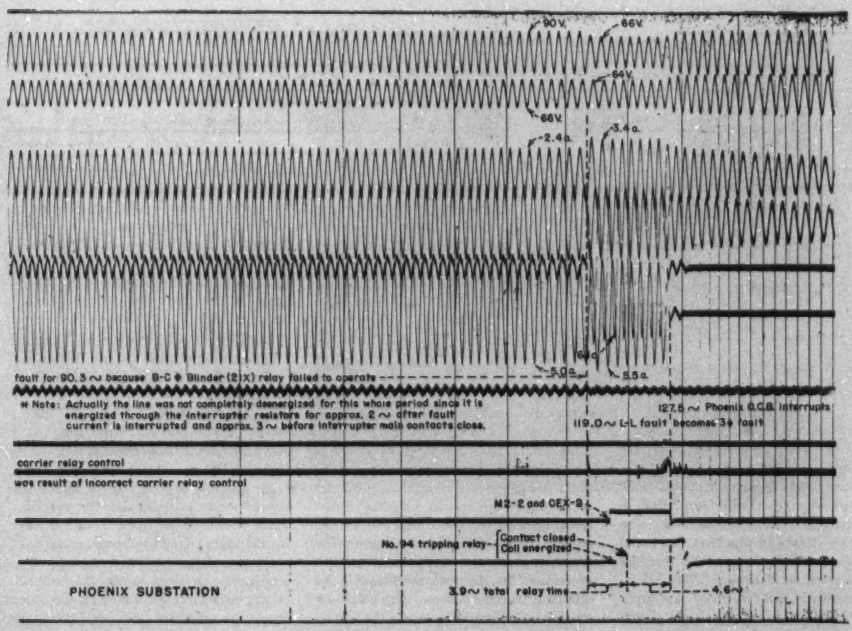


Figure 7. Oscillogram from Phoenix oscillograph for test 5, February 1–2, 1947. Three-phase fault on line 2 at Parker

Note slow operation of Phoenix phase relays, incorrect carrier-blocking signal on line 2, restriking of fault arc, and failure of Phoenix phase relays to operate on phase-to-phase fault



backup protection for *M2* with this arrangement as would be the case if all three elements were set to protect in the same direction, which is the practice where carrier-blocking is not used. The need for backup protection for the second element was proved during one of the February 1–2 tests. A line-to-line fault at Parker power plant was not cleared by the Phoenix phase relays. This fault was finally cleared when it developed into a 3-phase fault and caused the Phoenix phase relays for the other two phases to operate. Figure 7 shows the oscillogram for this test. The oscillogram indicates that simultaneous clearing of the staged 3-phase fault was not obtained and the 20-cycle reclosing of the first circuit breaker



to open re-established a line-to-line fault, phases *B-C*, as sufficient time had not elapsed for the deionized fault arc gasses to disperse. Figure 9 shows how, after about 90 cycles, the fault arc traveled upward and fortunately contacted the third phase in the transfer bus above, which finally caused the phase *A-B* and phase *C-A* relays at Phoenix to operate. Later, before the February 15–16 tests, it was found that the blinder relay for phase *B-C* on line 2 was binding and operated erratically on small currents. All the relays were checked over for operation on load current, and it was found that in some cases erratic operation was also caused by the sticky film that was found on all parts within the case and this film was removed with alcohol. This sticky film apparently was thrown off as a vapor from the insulation varnish on the potential coils during the short period of a few weeks that the relays had been energized prior to the staged fault tests. A cause of slow operation was the small fault current of 3.2-amperes in the relays. Operating time tests with currents of about three amperes were made on the directional distance and blinder relays, and the impedance reach of some of the relays was increased to speed up the relaying times.

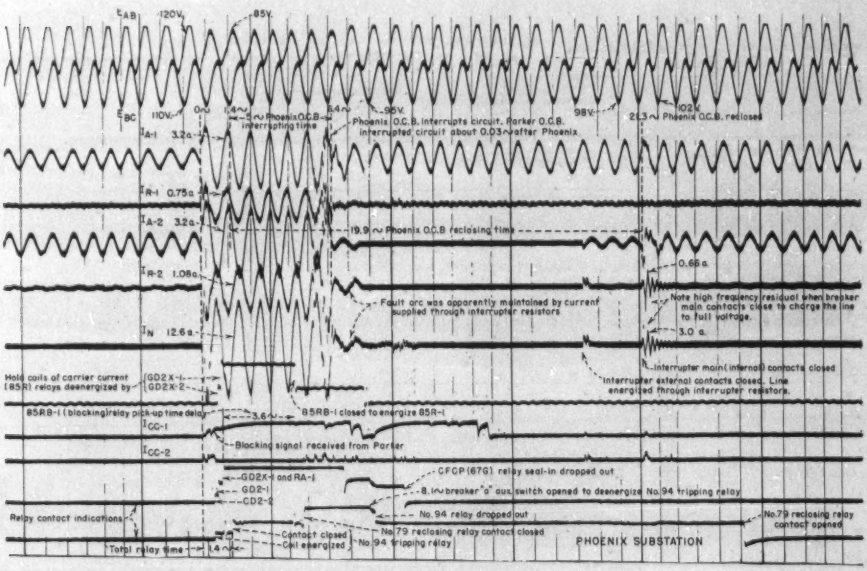


Figure 8. Oscillogram from Phoenix oscillograph for test 6, February 15–16, 1947

Phase *A*-to-ground fault on line 2 at Parker. The fault arc was maintained through the oil-circuit-breaker interrupter resistors after the main contacts opened. Note the 240-cycle transient currents when Phoenix oil-circuit-breaker main contacts reclosed to energize the 161-kv line

It is planned to increase the current in the long-line directional-distance relays by auxiliary-current transformers because of the small-fault currents available under certain conditions. The manufacturer has worked out a circuit arrangement involving the addition of another offset *M2*-type unit which will provide the desired backup protection for the *M2* element and will also simplify and speed up the tripping circuit for faults which require both *M2* and blinder-relay action.

It is believed that the present practice of reversing the direction of the third element when the special long-line directional-distance relays are used with carrier current should be reviewed because of its weakness from the standpoint of backup protection for the second element. An alternate relaying scheme, with emphasis on simplicity, would be to use the phase-comparison-, or *HKB*-type of carrier-current relaying which would protect for all but 3-phase faults in most cases, and to use the special long-line directional-distance relays for backup with all three elements set for the same direction. The 20-cycle reclosing circuit breakers would reclose whenever faults were cleared by the carrier-current relays, which would be for all faults except the 2 or 3 per cent of the faults which are 3-phase and which are probably most likely to be of a permanent nature when reclosing is not desirable.

Ultra High-Speed Reclosing Time as Effected by Low-Ohmic Resistors Paralleling Main Circuit-Breaker Contacts

The application of ultra high-speed reclosing circuit breakers to systems with higher voltages or for reclosing times faster than 20-cycles, may be reason for further study to determine the optimum value of resistance which should be used for the main-contact interrupter resistors of some modern oil circuit-breakers. The interrupter resistors in the 20-cycle reclosing circuit breakers used on line 2 are especially effective in preventing or

minimizing arc restrikes when interrupting line-charging current. However, the oscillogram in Figure 8, and others, show that the fault arc was maintained for an additional two or three cycles after the main contacts opened, although the fault current was reduced by the interrupter resistors. This encroaches on the time allowed for the deionized fault gasses to disperse from the faulted line between the time the main circuit breaker contacts open and before they reclose. It also brings up the question of whether the circuit breakers are 5-cycle circuit breakers, or 7- or 8-cycle, depending upon whether the time is taken as when the main contacts open or when the fault is actually cleared. It would appear that the oil circuit-breaker standards committee would be interested in this problem.

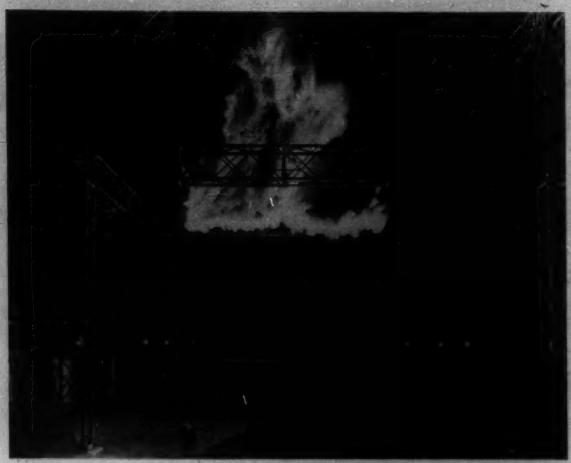


Figure 9. Fault arc, test 5, February 1–2, 1947

Initial fault was on all three phases of line 2 at Parker. See Figure 7 oscillogram for nature and duration of faults. Maximum fault arc current about 2,100 rms amperes

Time for Ionized Arc Gases to Move Upward or Disperse

Data as to the time it takes an arc to travel upward with little wind assistance may be obtained from Figures 7 and 9. Figure 9 shows the initial 3-phase fault which was applied to the jack bus and shows how the line-to-line fault arc, which occurred on reclosing, traveled upward until it contacted the third phase in the transfer bus above. The distance from the jack bus to the transfer bus above is 17 feet, and the time on the oscillograph was about 119 cycles. This information may be of interest in connection with vertical-configuration transmission lines.

Figure 10 shows the difference in photographed intensity due to the two line-to-ground faults being applied to the two phases at different times. The relative amounts of arc gases and deionized air probably correspond to the photographed arc sizes.

Table II provides data to illustrate the shortest time it may be feasible to allow as a de-energized time for reclosing-line circuit breakers. The spacing between phases in the jack bus is 12 feet compared with 17 feet between conductors on the 161-kv Parker–Phoenix transmission line. The times given in the table are for the intervals between the opening and reclosing of the main contacts. Since the circuit is energized through the interrupter resistors in the circuit breaker during part of this interval it would appear that these times should be reduced by two or three cycles to represent time fault is cleared, and possibly by four to six cycles, to correspond to actual de-energized circuit time.

Satisfactory Relay and Circuit-Breaker Operation

Oscillograms from both the Parker and Phoenix oscillographs for a double line-to-ground fault on line 2 at Parker after the

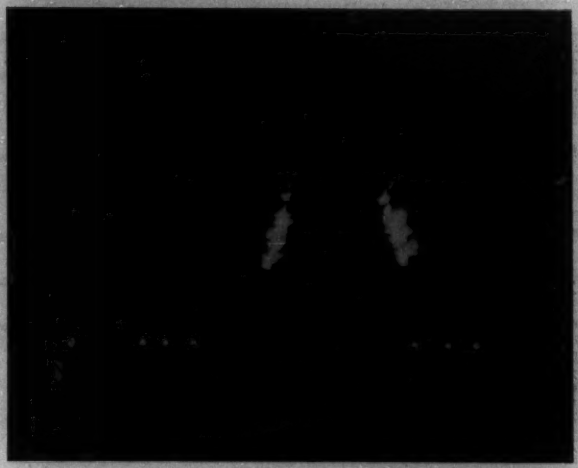


Figure 10. Fault arc, test 10, February 15–16, 1947

Double line-to-ground fault on line 2 at Parker. Maximum fault arc current about 2,600 rms amperes. The phase *A* fault arc was brighter due to longer duration. See Figure 11 oscillogram for detail on fault duration

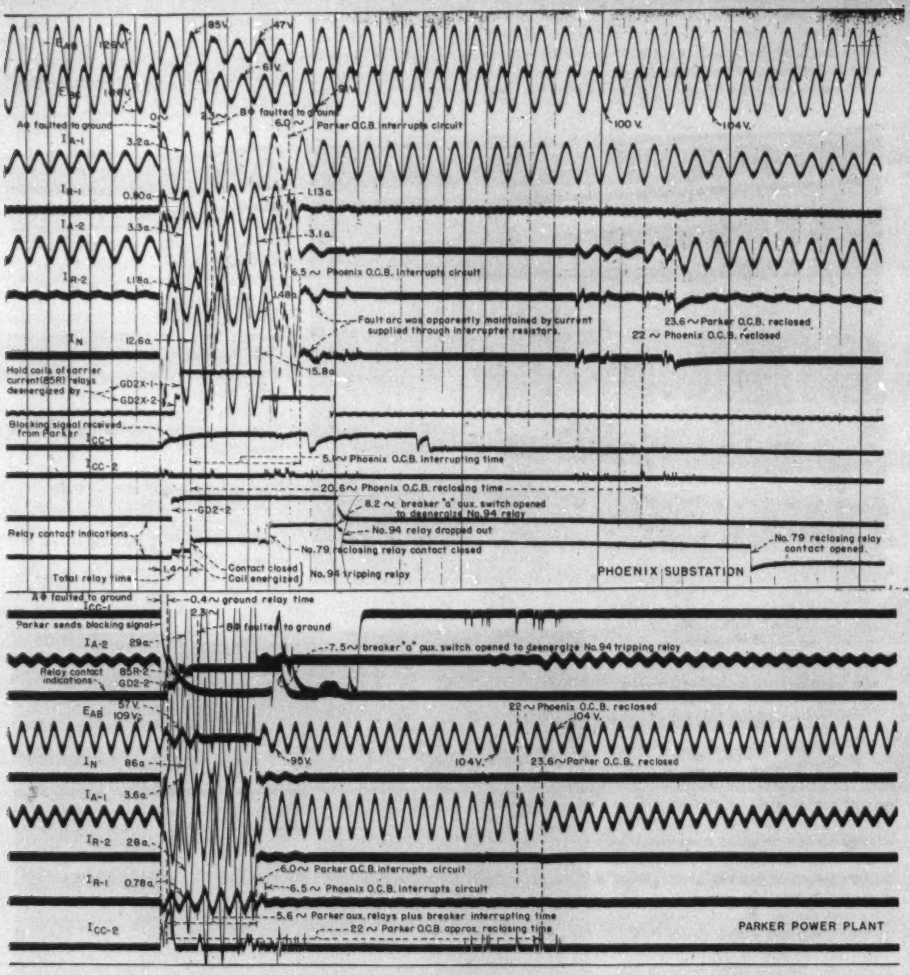


Figure 11. Oscillograms from test 10, February 15–16, 1947

Double line-to-ground fault on line 2 at Parker

preliminary modifications discussed previously had been made are shown in Figure 11. Correct relaying and carrier blocking occurred by action of the ground relays and the faulted line was cleared almost simultaneously at both ends. The reclosing circuit breakers energized the line from both terminals within about 1.6 cycles of each other.

The oscillograms from the February 1–2 tests made it possible to make the remedial measures under which the relays are now operating. After the permanent changes are made it is expected that the relaying will be faster for all types of faults and more reliable for possible minimum system conditions.

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1. Staged Fault Tests of Relaying and Stability on Kansas-Nebraska 270-Mile, 154-Kv Interconnection. C. W. Minard, R. B. Gow, W. A. Wolfe, E. A. Swanson. AIEE Transactions, volume 62, 1943, pages 358–67.

2. Application of the Ohm and Mho Principles to Protective Relays. A. C. van C. Warrington. AIEE Transactions, volume 65, 1946, June section, pages 378–86.

No Discussion

X-Ray Thickness Gauge for Cold-Rolled Strip Steel

W. N. LUNDAHL
ASSOCIATE AIEE

THE EVOLUTION of new and higher speed processes, new materials, and the trend toward greater precision and quality have necessitated development of more satisfactory gauging methods.

Most gauges now in use are of some electromechanical contact type. Many limitations arise due to the necessary maintenance of direct contact between the gauging head and the test material. Metals with finished surfaces tend to be scratched or marred at the contact point. Soft materials such as some plastics and paper would be torn by a contact type gauge. High temperature materials such as plate glass as it comes from the lehr (annealing furnace) are impossible to gauge continuously. Until recently, no satisfactory continuous gauging device had been developed for the steel industry's hot mill. The tremendous speeds and the high temperatures at which the modern mill operates have defied previously known gauging methods. Increasingly high speeds of cold mill operation also have increased the necessity of a noncontact gauge.

The combination of X ray and electronics lends itself admirably to many gauging problems. The outstanding advantage of this combination is that there is no physical contact between the gauge and the gauged material. Since it cannot scratch, there is little discrepancy due to paint and lacquer coatings; it is not subject to progressive shift in calibration due to mechanical wear or through accumulation of scale or oxides picked up from the material. It enables continuous gauging of soft or thin material, and limitations due to temperature theoretically do not exist. Satisfactory continuous high-speed gauging becomes practical since vibration and centrifugal force are not factors.

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W. N. LUNDAHL is design engineer with the Westinghouse Electric Corporation, Baltimore, Md.

Theory

Basically, X-ray gauging is accomplished by irradiating the material under test and in effect, measuring the drop in radiation intensity through that material. The form of the standard general absorption equation is

$$I = I_0 e^{-\mu \rho t} \tag{1}$$

where I_0 is the original intensity, I the transmitted intensity after filtration, μ the mass absorption coefficient, ρ the density of the material, and t the thickness of the material. The absorption coefficient is a function of the atomic number of the absorbing matter and the wave length of the radiation. It is independent of physical state, state of aggregation and temperature. This equation is theoretically applicable only to monochromatic X rays.

The heart of the X-ray thickness gauge is the pickup device which measures I quantitatively. Certain physical and chemical effects may be utilized as quantitative measurement of X-ray intensity. Four of these effects may be used with various degrees of practicability and success. These phenomena which result in various irradiated substances are

1. Heat content increase.
2. Photographic film exposure.
3. Gas ionization.
4. Fluorescence.

The first effect, which occurs where a material absorbs a large percentage of the primary radiation, appears as a measurable net increase in the heat content of that material. The second effect, commonly used radiographically, results from X radiation reducing silver bromide to silver in a photographic emulsion. The third effect is obtained when a gas, irradiated by X rays, becomes ionized to a degree proportional to the energy of the incident radiation. The fourth effect results in the excitation of luminescence in some types of irradiated substances.

For industrial applications of high-speed gauging it is obvious that the first two types of mensuration are not practical. Both the phenomenon of fluorescence and that of ionization, however, are quite satisfactory for high-speed application.

With the ionization principle, intensity is determined by measuring the flow of ionization current in an enclosed chamber. Utilizing the phenomenon of fluorescence the radiation is allowed to impinge upon a fluorescent material, and the resultant visible light emission is picked up by some highly sensitive photoelectric cell. The output of this cell is then proportional to the intensity of incident radiation upon the fluorescent screen.

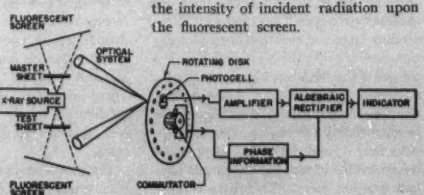

Figure 1. A one-source one-pickup system

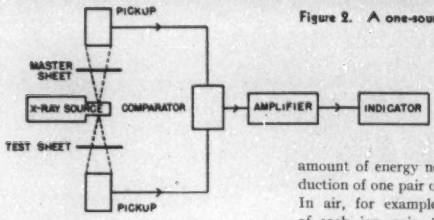

Figure 2. A one-source two-pickup system

The Ionization Chamber

Until recently, quantitative electrical X-ray detection and measurement were accomplished only with some form of the ionization chamber. This is mainly because the definition of the roentgen unit, used widely in radiology, is dependent upon the ionization of air. Basically, an ionization chamber consists of a metal tube a few inches in diameter and from two or three inches up to three feet or more in length. At one end is a window, consisting of a hole covered with a thin sheet of some material fairly transparent to X rays such as aluminum foil, mica, a plastic of some sort or beryllium. A metal electrode is mounted parallel to the axis of this cylinder but displaced from its concentric position to such a degree that X rays entering the window parallel to the main axis do not impinge upon the electrodes. This electrode is supported and insulated from the cylindrical chamber. With an external source of potential, the central electrode is made positive with respect to the cylindrical housing. In series with this potential source is a high value of resistance over which a potential drop indicates the magnitude of the ionization current.

It is essential that the potential be high enough to produce a saturation current so that all ions produced may be drawn across to the electrodes. The physical dimensions of this chamber, the gas used in the chamber, and the potential difference maintained between the two elements are dependent upon each other and upon the quantity and quality of the X rays being measured.

Although it is possible to ionize any gas with X radiation, the most effective gases for use in ionization chambers are those which contain a heavy atom. These produce large and easily measured ionization currents. As the X-ray beam strikes the gas in the chamber, high-speed photoelectrons are liberated. These photoelectrons have too much kinetic energy to be immediately absorbed by the gas molecules near it, and thus they break down the molecules in which they come in contact into ion pairs. There is a definite amount of energy necessary for the production of one pair of ions in a given gas. In air, for example, at the formation of each ion pair, 33 electron volts of energy are removed from the photoelectron, thus they dissipate their original kinetic energy until the electron is absorbed by a gas molecule to form a negative ion or to cause neutralization by absorption with a positive ion. When the metallic rod carries a positive charge with respect to the cylindrical chamber, the electrons will be collected by the positive rod, and the positive ions will move toward the chamber wall. This migration of ions, of course, constitutes the ionization current. The well-known Geiger-Mueller counter is a refined variation of the ionization chamber.

The Photomultiplier

Since its introduction by Hodges and Morgan[1] in 1941, the photomultiplier X-ray detector has been widely used in automatic exposure timing for mass medical radiography. This application is of the high intensity type due to the comparatively large quantity of light available. The photomultiplier has appeared to be rugged, simple, and fast but of questionable dependability for applications of high precision.

It may be well to describe the construction and operation of the photomultiplier. The photomultiplier is a high vacuum phototube whose enevelope contains not only the photosensitive cathode and the pickup anode but a number of additional elements called dynodes. Tremendous amplification is obtained by the utilization of the secondary emission principle. Each dynode is maintained at a progressively higher potential with respect to the photocathode. When a photoelectron is emitted from the photocathode, it is swept by the potential difference between the cathode and the first dynode where upon impingement upon this first dynode four or five electrons are splashed out by secondary emission. These secondary electrons are in turn accelerated to the next dynode where each splashes out four or five more electrons.

The most common of these photomultiplier types is the *RCA 931-A*. In this particular tube, there are nine dynodes; therefore, with the foregoing description of one electron splashing out four or five by secondary emission on the first dynode enables a theoretical total amplification in the order of 10^6. To give one the idea of the comparative sensitivity of this photomultiplier, the RCA Tube Handbook, volume 5-6, gives a sensitivity at 4,200 angstroms and 100 volts per dynode of 8,200 microamps per microwatt, or a luminous sensitivity of ten amperes per lumen. In contrast, the *929 type* of high vacuum phototube, which contains just the two elements, the photocathode and the anode, has a sensitivity at 4,200 angstroms of 0.037 microampere per microwatt, or a luminous sensitivity of 45 microamperes per lumen.

To convert this photomultiplier to a simple radiation pickup device, the envelope of the tube is surrounded with a sheet of X-ray fluorescent screen, and in turn is covered by black paper or other covering to keep out extraneous light.

A fluorescent material absorbs photons of high energy such as X-radiation or ultraviolet rays. These high energy photons, which are absorbed by the fluorescent material, impart a portion of their energy in the form of lower energy photons that constitute visible light. Fluorescent materials may also emit visible light under the action of high energy particles of cathode rays, alpha and beta rays. The chemistry of fluorescence is lengthy and involved. The characteristics of the screen such as basic visible light wavelength and intensity depend upon the composition of the chemicals in the fluorescent material. At this point, it may suffice to say a fast screen is needed; that is, one with a high intensity but with very little afterglow. Also, the wavelength of the visible light should be near that wavelength to which the phototube is most sensitive. A Patterson type *B* zinc-cadmium sulfide screen which represents a compromise in the necessary characteristics is commonly used.

Basic Geometrical Arrangements

It may be well at this time to discuss the various physical arrangements of the components utilized in the gauging process. Basically, all that need be involved is a single source of X radiation and an ionization chamber or photoelectric pickup placed in such a position that it may receive the transmitted radiation emerging from the test material. Practically, however, this method allows cumulative errors to destroy any semblance of dependability and precision.

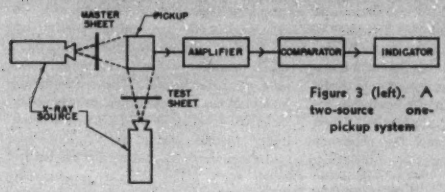

Figure 3 (left). A two-source one-pickup system

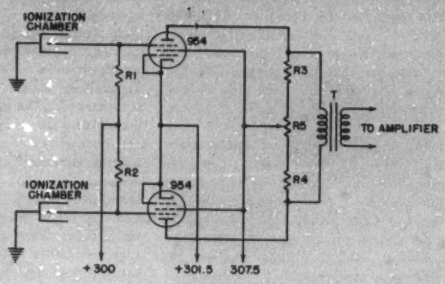

Figure 4. Ionization chamber input circuit used experimentally with a one-source two-pickup system

Characteristic drifts in amplifier circuits and other allied apparatus, X-ray intensity changes due to voltage and current changes in the X-ray tube, and thermal effects all contribute toward large errors. Thus, it becomes necessary to utilize some comparison scheme where the signal obtained from the test piece may be compared to some standard signal, the complete unit having common parameters.

There are three primary methods of physical arrangement that enable greater accuracy by the progressive cancellation of cumulative errors. The first method is shown in Figure 1. This particular type utilizes one X-ray tube with two parts, two fluorescent screens mounted so as to receive the transmitted radiation from the test and master strips and an optical system for transmission of the visible light from the fluorescent material to a rotating shutter disk, which allows light resultant from a master sheet and a test sheet screen respectively, to impinge upon the photocell's sensitive surface. The shutter disk is rotated at a high-speed, and its openings alternately expose the phototube to light emitted from the test sheet fluorescent screen and from the master sheet fluorescent screen. The signal from the phototube amplifier is applied to some type of algebraic rectifier where the polarized d-c response is obtained for operation of a thickness indicating instrument, roll pressure adjustor or reject mechanism. A commutator mounted solidly on the rotating shutter disk provides phasing information for the algebraic rectifier. This system is objectionable because of the somewhat complicated mechanical arrangement and dependence upon synchronism between the electrical and mechanical components.

The second method involves the use of two pickup devices and one X-ray source as shown in Figure 2. This geometrical scheme may utilize either the ionization principle or the fluorescent principle for quantitative measurement. A double window X-ray tube irradiates a pickup through filtration of a standard absorbing material from one window and irradiates another pickup through filtration by a test material. In this case, the X-ray source operates self-rectified; that is, raw high potential alternating current is placed across its elements, and the tube emits pulses of radiation for somewhat less than one-half cycle when the anode of the X-ray tube is positive with respect to the cathode. Thus, radiation pulses appear at the output terminals of the pickup devices simultaneously. These pulses are amplified and added algebraically with the resulting difference indicating the thickness difference between the master and the test material. It is quite difficult, if not impossible, to maintain dependable accuracy with this system due to the problem of maintaining constant and identical pickup outputs especially with the photoelectric type of pickup.

A third method utilizes two sources of X ray and one pickup device. This scheme is laid out in block form in Figure 3. In this case, both X-ray tubes are so arranged that their transmitted radiation after filtration from a master and a test piece, respectively, are applied to a single pickup device to enable subsequent electronic devices to differentiate between a standard signal and a test signal. The two X-ray tubes are so connected that they operate 180 degrees out of phase. Therefore pulses from each source arrive alternately at the common pickup. This method will be discussed in detail later.

The distance between the pickup and the X-ray source is important. The drop in intensity through air follows the inverse square law; thus, the intensity four inches away from the source would be 1/4 that of the intensity two inches away from the source. In order to keep the original intensity down to reasonable values for personnel safety and low X-ray tube loading, it is important that the pickup be kept near the source.

An Experimental Ionization Chamber Gauge

Much information and experience have been obtained in the X-ray gauging problem from work on an experimental ionization chamber thickness gauge. The basic scheme was that of number 2 mentioned previously; that is, one which used one X-ray source and two pickup units. The chamber only differed from common design in that a small acorn pentode vacuum tube was mounted in the

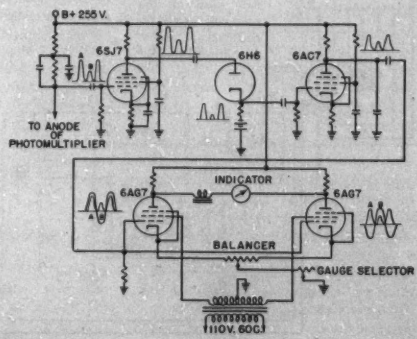

Figure 5. Gauge circuit used in the cold-mill thickness gauge

end opposite the beryllium windowed sensitive end. Dry air at atmospheric pressure was used as the ionizing medium.

The input circuit, Figure 4, utilizes the acorn pentode in an unorthodox manner to obtain electrometer tube operation. Usage and potentials of the various elements of the tube are such that absolutely no current is drawn from the ionization chamber by the control grid. The resistance values of $R1$ and $R2$ were in the order of 100 megohms to provide sufficient voltage for the control grid. The combination of $R3$, $R4$ and T acts as the differential comparator.

There are several obvious disadvantages of the ionization chamber type of thickness gauge as applied to an industrial layout. The output of the ionization chamber is extremely small; therefore, considerable amplification is necessary to obtain signals which can be handled in standard circuits. Thus, high gain amplifiers with their attendent difficulties are necessary. The high value of load resistance of the ionization chamber, together with its comparatively high capacity, combine to give a large time constant which will adversely affect the speed of response.

The Cold Mill Gauge

The Westinghouse X-Ray Division has recently completed development and design of an X-ray thickness gauge for cold mill application utilizing one photomultiplier pickup and two X-ray sources. The gauge, whose range is from 5 to 50 thousandths of an inch, attains accuracies up to 1 per cent.

The complete gauge consists of an X-ray source and pickup unit, a main-control housing, a remote control panel, and an indicator unit. The X-ray source and pickup unit consists of two separate X-ray sources and the housing for the photomultiplier tube. The main control housing contains all the necessary electronic equipment for the gauging operation together with auxiliary apparatus. Upon the remote-control panel the necessary controls for routine operation of the gauge are mounted. The indicator unit is the instrument which indicates thickness above or below the standard gauge being run. It contains a large switchboard type zero-center milliammeter and indicating lights for giving reject information.

The X-ray source consists of a self-contained oil immersed X-ray generator. The X-ray tube, which operates self-rectified, has a tungsten target. The tube current is maintained at 0.5 milliamperes and the tube voltage is varied between approximately 20 and 50 kilovolts, depending upon the gauge of steel being run. The pickup device which is the photomultiplier with a fluorescent screen surrounding its envelope is mounted in a brass housing located approximately two inches above the upper surface of the steel sheet. The standard X-ray source is located approximately ten inches horizontally from the photomultiplier. Its radiation is directed axially along a cylindrical brass X-ray cone through a standard sheet to the photomultiplier. The other X-ray source is located beneath the test sheet. Its radiation is directed upward through the sheet to the photomultiplier. The distances between the photocathode of the photomultiplier and the target of the two X-ray sources are equal. A photomultiplier is so mounted that a projection of its sensitive surface bisects the right angle formed by the intersection of the radiation from the two heads. Since in the cold mill application, X-ray source units and the pickup unit will be operating in an atmosphere with a heavy concentration of water vapor, oil, and scale, all electric connections are made waterproof. The control housing may be mounted in any convenient location up to 150 feet or more from the pickup unit. Each basically distinct circuit is wired in a separate chassis. These chassis are mounted on a rack which swings away from the front panel to facilitate accessibility to the wiring.

Interconnections between chassis are made with cable plugs to enable the serviceman to quickly change any particular unit that may need repair. These chassis contain the main gauge circuit and its power supply, a photomultiplier power supply, a selective reject circuit and a tandem-milliampere stabilizer.

The Gauge Circuit

The gauge circuit, Figure 5, consists essentially of a pentode-detector amplifier, a duplex diode clipper and base line restorer, a pentode saw-tooth converter and a pair of power amplifier pentodes used in a comparator circuit. A pentode photomultiplier stabilizer and a cathode ray indicator amplifier are also located in the gauge chassis.

The output signal obtained from the anode of the photomultiplier is capacitor-coupled to a conventional stage of pentode amplification. The amplified signal from the first stage is coupled to the clipper section of the duplex diode where a portion of the signal is conducted through to the following stage. The percentage of the signal amplitude which is conducted depends upon the bias maintained across the tube elements. This stage serves two purposes. Sensitivity selection is obtained simply by varying the bias across the elements. As more of the lower portion of the signal is clipped the percentage amplitude differential between the standard pulse and the pulse representing instantaneous pass-line thickness increases, thus increasing the thickness sensitivity. Thickness sensitivity is defined as the percentage thickness differential between the standard sample and the

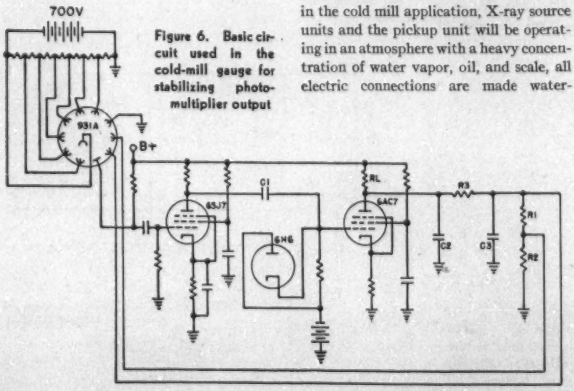

Figure 6. Basic circuit used in the cold-mill gauge for stabilizing photomultiplier output

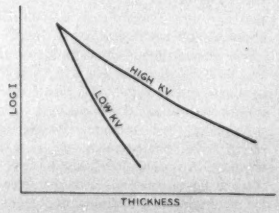

Figure 7. Absorption of X rays at two different X-ray tube voltages

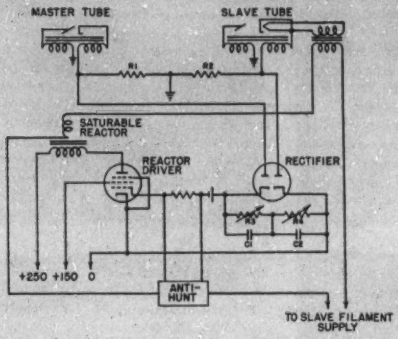

Figure 8 (left). Tandem milliampere stabilizer used for maintaining identical X-ray tube currents

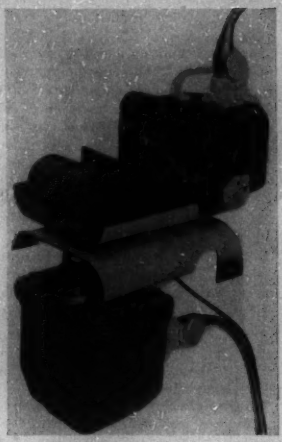

Figure 9. X ray source and pickup unit for cold-mill thickness gauge

instantaneous pass line thickness which will give a full scale thickness indicator deflection. A second function of the diode is to prevent damaging currents from flowing through the indicator's movement in the event of large thickness differentials or complete removal of the pass-line strip.

The cathode of the clipper is coupled to a pentode saw-tooth convertor which in effect adds area to the pulses. The average value of the signal is quite small due to the short duration of the pulse thus the additional area derived from the saw-tooth wave form materially increases the effectiveness of the following stage.

The saw-tooth conversion is followed by the *6AG7* pair in a comparator circuit. This circuit operates in a somewhat unconventional manner. The control grids of the two tubes are in parallel. The cathodes are carried to ground through a potentiometer balancer and the gauge selector rheostat which is mounted in the control panel. The plates are carried through identical load resistors to $B+$. The screen grids are connected to the opposite ends of the secondary of a transformer whose center tap is grounded. Assume that at given instant pulse A has arrived at the control grid of the two tubes. At that same instant, the phasing of the alternating potentials on the screen grids are such that the left hand tube screen grid is at a positive peak. The right hand tube screen grid is 180 degrees out of phase, and thus at a negative peak. Since a pentode will not conduct when the screen is at zero or at a negative potential, no plate current will flow in the right hand tube. However, the left hand tube will conduct—the plate current being a composite a-c wave but instantaneously a current of a value depending upon the peak amplitude of the signal on the grid at that moment, the instantaneous alternating potential on the screen, and the tube characteristic. One-half cycle later, pulse B has arrived on the control grid. The right hand tube screen has become positive and the left hand tube screen has become the same value negative, and conduction occurs only in the right hand tube. Thus, the tubes become selective; the left hand tube operating with the pulse A and the right hand tube operating with pulse B. This circuit is in effect a bridge circuit in which the two plate load resistors and the two tubes act as the legs in the circuit. If a suitable instrument is placed between the two plates, any unbalance in the circuit will be indicated by a deflection proportional to the amount of unbalance. This unbalance, of course, will be caused by differences in amplitude of pulse A and pulse B. If the pulse amplitudes of A and B are equal, the voltage drops across the tube-plate resistors will be equal. Thus no direct potential will appear across the tube plates; however, if the pulse amplitudes differ, the plate currents of the two tubes will of course differ, thus, the voltage drop in the plate load resistors will differ and in effect a direct potential will appear across the tube plate and be indicated by the deflection in the proper direction of the indicating instrument. This indicating instrument is the instrument which indicates the thickness of the steel above or below the gauge of the standard piece. A high reactance choke is in series with the coils of the instrument to serve as a filter preventing alternating current from being applied to the movement.

The Photomultiplier Stabilizer

It is well known that high vacuum phototubes employing the secondary emission principle such as the *931-A* described previously are subject to drift in their characteristics. This drift occurs in varying degrees during tube operation and over the tube's life. Since this tube is the heart of the thickness gauge, it is apparent that the aforementioned drifts cannot be tolerated if the instrument is to remain accurate.

The circuit used in this particular gauge maintains phototube output constant by automatically controlling the dynode potentials and in effect controlling the tube's amplification. The photomultiplier picks up the signal from the visible light emitted by the fluorescent screen. The pulse is amplified throughout the phototube and then applied to one stage of external amplification. This pulse is then applied to a circuit which determines whether or not the amplitude is within the preset range. This compensator circuit then adjusts the power supply of the photomultiplier to a voltage output which will enable the photomultiplier to put out a signal of the predetermined standard amplitude. Thus, if the tube tends to drift to an increased amplification, the compensator circuit reduces the power supply voltage to the tube and the drift is automatically cancelled.

Figure 6 is used for a more detailed discussion. The circuit contains the following components: a photomultiplier tube, a standard pentode amplifier stage, a sharp-cutoff pentode, a π-filter network, a bias supply for the sharp-cutoff pentode, and a base-line restorer.

A pulse from the *931-A* is amplified by the pentode. This amplified pulse is applied to the grid of the stabilizer tube, which is the remote cutoff pentode,

through coupling capacitor $C1$. However, the stabilizer tube is biased far beyond cutoff, and thus it will not conduct unless the signal pulse amplitude nearly equals the d-c bias potential. If the stabilizer tube conducts, the average IR drop across R_k increases. Thus, the drop across $R1$ and $R2$ decreases. The pulse signal appearing across the tube is effectively filtered out by the π network of $C2$, $R3$, and $C3$. At times, the signal amplitude increases to quite high values due to thin test material or complete removal of the test material. This high signal amplitude will cause the grid to swing positive throwing the response beyond the linear portion of the grid characteristic with possible injury to the tube. The diode will tend to clip off the lower portion of the signal as seen by the grid of stabilizer tube, thus reducing the average a-c value. Since the average a-c value of the signal swings around the d-c-bias potential, the peak values will remain within the linear range. The base line reference is thus restored to its original position.

Calculations and experiments have shown that normal drift of any photomultiplier of the particular type under discussion easily can be compensated for by a maximum plus or minus variation of potential of 40 per cent across the eighth and ninth dynode. As shown in Figure 6, the dynode potential application is so designed that the seventh element is grounded. A constant high voltage supply feeds the bleeder from the seventh element to the cathode. The eighth and ninth dynodes are fed from the divider $R1$, $R2$, and the anode is supplied through a resistor from the common B supply for the vacuum tubes and the gauge circuit.

The signal input to the phototube is set originally so that an optimum signal current flows through the stabilizer tube. This sets the voltage across $R1$, $R2$ in its mid-value. If the tube tends to drift down in amplification, the pulse on the grid of the stabilizer tube becomes smaller. The voltage increases on $R1$, $R2$, and also on the eighth and ninth dynodes. The amplification increases again until the compensator circuit reaches equilibrium. Conversely, if the signal becomes too large due to a positive drift in $T1$, more voltage is peeled off the filter network. The eighth and ninth dynode potentials drop until balance is again obtained. Another desirable feature of this device is the fact that complete removal of the test sheet and the consequent subjection of high intensity radiation directly upon the pickup device does not injure the photomultiplier. The instant the test sheet is removed, the stabilizer automatically drops the eighth and ninth dynode potentials to a value low enough to prevent damaging currents from flowing.

This gauge as described is essentially a percentage reading device. However, the demands of industry are such that instruments of mensuration should not require conversion tables or conversion factors but must be of the direct reading type. There are certain circuit parameters which enable adjustment of sensitivity which subsequently could be used to obtain a direct reading instrument. The addition of a rheostat in the cathode circuit of the comparator provides a linear gauge selection control. Consequently, the operator may select the gauge of stock to be run by a simple adjustment of this calibrated rheostat at the remote control panel.

Selecting X-Ray Intensity

The X-ray source will be operating continuously, and thus the rating of the X-ray tube, which is generally the weakest link, must not be exceeded. The intensity of the X-ray beam is directly proportional to the current flowing in the X-ray tube and approximately proportional to the square of the voltage across the elements of that tube. Thus, in order to keep the losses down, it is possible to operate the tube at a very low current and obtain the intensity necessary for correct excitation of the photomultiplier by kilovoltage adjustment. In the cold-mill gauge, the X-ray tubes are operated at a constant current of one-half milliampere, with tube voltages varying from 20 to 50 kv dependent upon the gauge of the material being run.

Figure 7 shows the filtered X-ray intensity versus the thickness of a filtering medium at two different voltages. This plot shows logarithmically the change in filtered intensity due to a thickness change in the filtering medium, using equal initial intensities. This plot is derived indirectly from the exponential absorption equation. The wave length distribution, hence the absorption coefficient, is a function of tube voltage. It may be concluded from inspection of the curve or from inspection of the equation of the curve that the lower the voltage, the greater the sensitivity. A limitation exists, however, to prevent obtaining infinite sensitivity even under optimum conditions. The limitation arises first in the pickup device. This limitation is due to noise arising before the first stage of amplification in the photomultiplier. Statistical fluctuations in the number of incident X-ray quanta and dark current electrons arising in the initial stage of the photomultiplier result in this noise. Dark current is the weak output of a

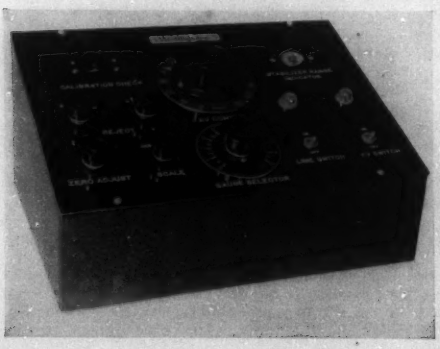

Figure 10 (left). Control panel for cold-mill gauge

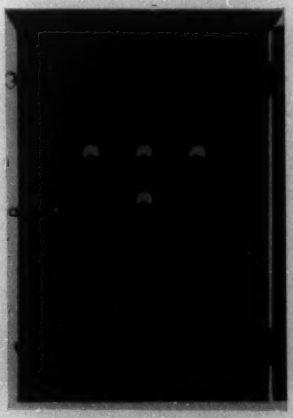

Figure 11. Main control housing showing instruments used for checking gauge operation

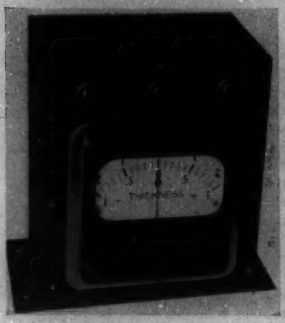

Figure 12. Thickness indicator shows thickness differential above or below standard directly in thousandths of an inch

photoelectric cell when no light falls upon it. This current arises from thermal emission and it could be reduced to negligible proportion by cooling the photomultiplier with liquid air or dry ice.

Optimum conditions include the use of the fluorescent screen causing the highest photomultiplier response, restriction of the X-ray beam to that small area on the fluorescent screen most effective in causing photomultiplier response, use of X-ray wave lengths for which absorption in the fluorescent screen is nearly complete, and use of a refined *931-A* for maximum light sensitivity.

It may be concluded then that for any given thickness of a certain material, there is one optimum kilovoltage for attainment of maximum sensitivity. The strict application of the absorption equation, however, is for monochromatic X rays only. In using a simple self-rectified X-ray generator with no filters as explained before, resultant X rays are heterogeneous in nature. Calculations corroborated by experiment have clearly shown that kilovoltage is not a highly critical factor in thickness gauge sensitivity due to wave length distribution as integrated over one pulse of radiation. Thus, voltage selection for a given gauge of material involves only two factors: One, obtaining a satisfactory optimum photomultiplier output and two, setting the photomultiplier stabilizer in the midpoint of its range.

The Tandem Milliampere Stabilizer

Operating the X-ray sources without any means of stabilization results in undependable gauge indication. X-ray tube current is dependent upon filament temperature, of course. If tube currents in the two heads are originally set at equal values, the possibility is quite remote that at the end of an hour's time the tube currents would still be equal. It is therefore necessary to automatically maintain identical current magnitudes at all times. This is accomplished with a tandem milliampere stabilizer. This electronic device maintains current equality by using current through one tube as reference and having the current of the other tube follow the former.

Briefly, the stability is obtained by comparing the two currents and using the voltages produced in the comparison to bias a control tube which varies the magnitude of a series reactance in one filament power supply which we may call the slave tube. Thus, if the slave current changes with respect to the reference or, master, the bias on the control tube will change in such a manner as to adjust the reactance to a new value which will in effect allow the correct anode current to flow. Referring to Figure 8, the operation briefly follows. Anode current from each tube is brought to ground through two equal resistors. Potentials proportional to the two currents are picked off *R*1 and *R*2 and rectified by a duplex diode, which prevents the negative portion of current wave due to distributed capacitance in the transformer winding from entering the comparison. This diode charges capacitor *C*1 and *C*2 to voltages proportional to their respective currents.

Rheostats *R*3 and *R*4 adjust the time constants of the two *RC* circuits which enable the operator to vary the magni-

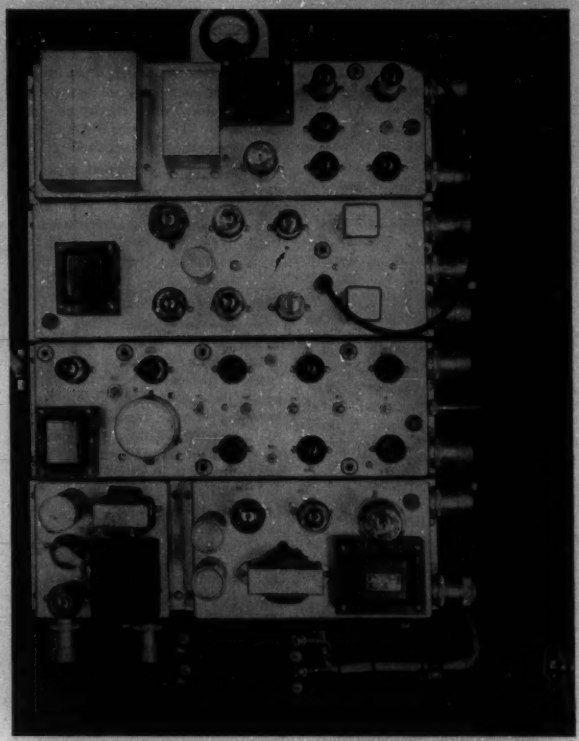

Figure 13. Service is facilitated by dividing the various basic circuits into separate chassis and using plug and cable connections between chassis

Circuit operation may be checked by inserting the milliammeter plug into jacks located in various stages

tude of the slave current. The bias on the control grid of the pentode is the algebraic sum of the two capacitor voltages and the bias battery voltage. This pentode is preferably one of high transconductance such as the *6AG7*. Plate current flows through the d-c winding of a saturable reactor. It can be seen that the alternating voltage drop across the reactor is controlled directly by the plate current of the pentode; that is, the greater the plate current, the smaller the voltage drop which results in a higher current in the slave tube. If anode current in the master tends to increase, voltage across *C*1 will increase tending to make the grid more positive which in turn allows more plate current to flow. This condition reduces the voltage drop across the reactor, thus increasing current in the slave to a point where balance is again obtained. An antihunt device using a feed-back transformer and a duplex diode rectifier, capacitor coupled to the grid circuit prevents oscillation due to high sensitivity and quick response to small current changes.

The entire X-ray generator is stabilized by an induction regulator. Voltage applied to the X-ray tubes must be carefully equalized to obtain coincident intensities of the resultant heterogeneous radiation. The variation in inherent filtration of the X-ray generators themselves due to slight differences in X-ray tube window thickness, oil surrounding the tube and head window thickness result in slight deviations from equality with equal increments of kilovolt change on the two X-ray tubes. Therefore, with changes of thickness of stock to be run, which require changes in tube voltage, slight rebalancing is usually necessary. With matched heads, these changes are so slight that a small X-ray current adjustment will satisfactorily correct output intensities with no adverse effects to accuracy. The addition of a solenoid operated slide mechanism, which inserts a preselected gauge of stock into the test beam, enables quick checks on gauge calibration. This device is mounted directly above the test sheet head.

An electronically-operated reject circuit operates from the output of the comparator circuit. The relays operated from this circuit close at predetermined thickness differentials for operation of reject mechanisms, alarm bells, or lights.

Personnel Protection

The question is often asked "What danger exists from X radiation to the operating personnel?" In the cold-mill gauge, there is no way possible for the operating personnel to be directly exposed to the primary radiation. The only danger that might exist is from the refracted and reflected X rays or scatter. The direct radiation is of an intensity low enough that the scatter radiation two inches from the point of the test sheet being irradiated is of an intensity much lower than the maximum dosage rate as given by the American Standards Association. In a hot-mill gauge, where the steel thicknesses are sometimes over a quarter of an inch, and the X-ray source-to-test sheet distance must be quite large due to the temperature of the sheet, the intensity of scatter will be proportionately greater. However utilization of proper protective means will eliminate any danger. The X-ray thickness gauge as in any X-ray application must utilize personnel protection proportional to the power of the generating unit involved.

Other Gauge Applications

There are hundreds of applications for an X-ray thickness gauge. Among them are measurement of the thickness of linoleum, plastic, sheet glass, many types of sheet metal, and paper and cardboard. One interesting application of a gauge which wouldn't work was that of counting bank notes. The bank notes were to be in bundles of 100, 500, or 1,000. The thickness gauge was required to indicate the number above or below that standard number in a given bundle. It sounded simple until it was found that the thickness of single bills was not uniform. They varied ±10 per cent. Thus, the indication as given by the gauge for the extreme condition would be 10 per cent from the actual number in the bundle.

The gauging of glass container wall thickness is readily accomplished with a variation of the cold-mill X-ray gauge. This application consists of scanning the container's wall with the object of locating thin portions which would tend to reduce the strength of the container.

Conclusion

Although each geometrical system has certain advantages over the others, it has been found that the two-source one-pickup system offers outstanding advantages in stability and precision. The photomultiplier pickup, sometimes of questionable value as a precision device, becomes a remarkably accurate instrument for quantitative radiation mensuration when properly selected and stabilized.

The combination of properly designed gauge and auxiliary electronic circuits and the remarkably adaptable photomultiplier pickup makes the X-ray gauge an important new tool in many manufacturing processes and industries.

Reference

R. H. Morgan. *American Journal of Roentgenology and Radium Therapy*, volume 47, 1942, page 777.

No Discussion

Suddenly Applied Loads Carried by a Variable-Ratio Synchronous–Induction Frequency Changer

A. G. DARLING
MEMBER AIEE

G. A. KAUFMAN
NONMEMBER AIEE

Synopsis: The induction machine of this frequency changer has demonstrated its ability to start and carry rotating machine induction loads requiring 140 per cent of the kilovolt-amperes of the induction machine to start them without benefit of capacitive excitation. A-c amplidynes are used for automatic voltage regulation of the induction machine. D-c amplidynes are used for regulating the load through the frequency changer. So far as the authors are aware, this is the first commercial installation of this type to incorporate these features.

General Conditions

THE PITTSBURGH WORKS of the Jones and Laughlin Steel Corporation generates its own power at 25 cycles and 6,600 volts. The power house includes three 20,000-kw and one 10,000-kw turbo-generators.

The power demand at the plant is as high as 46,000 kw. The major single item of this load is the hot strip mill. Every minute or so, when this mill is in operation the load rises 20,000 to 25,000-kw in five to seven seconds, stays at that level for three to five seconds, and then is reduced by the same amount in five to seven seconds. Among other loads, there is approximately 5,000 kw of essential load, such as the water supply pumps and so forth. Any interference which cuts off the power supply to this essential load, may cause heavy damage to the plant equipment. For instance, the water-cooled furnaces, deprived of water, may be put out of commission for a long period of time with an immeasurable financial loss to the steel company.

The company's power house is located in the flood danger zone. While it is not very probable, it is still possible that an extremely high flood would shut down the power house entirely, endangering the servicing of the essential load.

It was decided, therefore, to provide a tie with the Duquesne Light Company in order: (*a*) to obtain some 5,000-kw emergency service for essential loads in case the Jones and Laughlin power house should be shut down, and (*b*) to take a certain share of the total mill load.

Since the power company's system is 60-cycle, a frequency changer is required to provide the necessary link between the two systems. From the foregoing requirements it is obvious that the frequency changer should be capable of supplying, in emergency cases, the 25-cycle essential load acting at that time as a stub feed. At other times, the frequency changer should permit the regulated flow of power from the 60-cycle to the 25-cycle system, as required. The reverse flow of power from the 25-cycle steel company's power system to the 60-cycle public utility, is possible although it is not essential for this installation.

The power contract is such that it is advantageous to take power at a constant-demand rate. Without some form of control of the power flow through the frequency-changer set, a fairly large proportion of the load changes of the steel mill would be passed on to the public utility. For this reason, control of power flow through the set becomes an economic feature.

Selection of Type of Frequency Changer

At least three types of frequency changer sets were available: the synchronous-synchronous, the electronic, and the variable-ratio induction—synchronous set with, in this case, Scherbius control of the variable ratio.

In this instance, dimensions, weights, and efficiencies entered into the selection to only a minor degree. The characteristics which appear to control the selection can be briefly stated.

The fixed ratio or synchronous–synchronous set, would pass on to the power company a major portion of the load changes occurring in the steel plant. In order to control load changes through the frequency changer set, use of this type of frequency changer would necessitate control of the steel plant power generation in terms of the load through the frequency changer set, because there was no hope of controlling the generating sources of the large, widely dispersed public utility system. To accomplish the control of the four steel plant generators totaling 70,000 kw, it would have been necessary to equip their inlet steam valves with servomotors and make sure that the boiler pressure was not varied too fast or too widely. This would have been an expensive and somewhat intricate control to be certain that the steel plant generators not only took the load, but took it in proportion to their individual ratings. The synchronous–synchronous set is perfectly capable of furnishing stub-feed load and reactive kilovolt-amperes to either frequency system.

The electronic[1] set differs widely from the rotating types of frequency conver-

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A. G. DARLING is a member of the industrial power division, industrial engineering divisions of the General Electric Company, Schenectady, N. Y. and G. A. KAUFMAN is chief electrical engineer, Jones and Laughlin Steel Corp., Pittsburgh, Pa.

To P. W. Robinson and F. N. Bauman goes the credit for the design of the frequency changer and to F. E. Crever (M '45) for the design of the rheostatic voltage and amplidyne voltage and load regulators. J. D. Neeley and J. E. McNamara (A '45) supervised the installation and testing of the entire equipment.

ters. An electronic converter passes power through it to the system of the lower voltage and in proportion to the deviation from the nominal voltage ratios of the two systems. The flow of power through the frequency changer can be controlled by observing its direction and amount and modifying the grid firing angle to correct for deviation from the desired power flow. Some developmental design would be required to make it serve a stub feed load at either frequency. This equipment, for stub-feed operation, requires a-c excitation for both the rectifier and the inverter which takes the form of synchronous condensers operating on each frequency and equipped with starting motor of opposite frequency. When operating in parallel with other synchronous generators and motors, these machines can supply the necessary a-c excitation provided their capacity, in that respect, is not already used. These additional features for the electronic converter make it more expensive and less applicable for this installation.

The induction–synchronous variable-ratio frequency changer, when supplied with Scherbius type of excitation for the induction machine, can have its frequency ratio varied and can also supply stub-feed load from both the induction and the synchronous ends. When connected between two generating systems, the load through the set can be kept constant by varying the slip of the induction motor through the Scherbius control. In this case, the variable-ratio Scherbius-frequency changer set met all the conditions of the installation.

Accordingly, a 12,500-kw size set, Figure 1, whose major machines have been successfully used for a number of years at the Hudson Coal Company's plant near Wilkes-Barre, Pa., was found to apply adequately for the Jones and Laughlin installation. This set consists of a 15,000-kva 0.9 power factor, 13,800-volt, 60-cycle synchronous motor; a 13,500-kva 1.0 power factor, 6,600-volt, 25-cycle induction generator; a 1,200-kva Scherbius regulating machine and 33-kva a-c exciter and a 60-kw, 250-volt, d-c exciter for the synchronous motor. Amplidyne load and voltage regulators, capable of compensating for rapid changes constitute portions of the full automatic control of the frequency changer.

Power System

The power system of the Jones and Laughlin installation is illustrated in Figure 2. This diagram shows the frequency changer set located at the cold-strip mill connecting it to the public utility system.

Figure 3 (opposite). Stud feed tests of induction generator

Chart *A*—Induction generator voltage
Chart *B*—Induction generator frequency
13—2,200-kva synchronous motor started
14—3,900-kva synchronous motor started
15—Voltage applied to both sets; voltage collapsed
16—3,900-kva motor started
16*a*—3,900-kva motor synchronizing
17—2,900-kva motor started with 3,900-kva motor on line
17*a*—2,900-kva motor synchronizing
18—Both motors shut down, Chart speed 3 inches per minute

Figure 4 (opposite). Frequency changer in parallel with 60- and 25-cycle generators and under control of load to 25-cycle system

Chart *A*—15-cycle frequency
Chart *B*—60-cycle frequency changer load
Chart *C*—25-cycle voltage
Chart *D*—25-cycle turbine generator load
Chart *E*—25-cycle frequency changer power factor chart, chart speed 3 inches per minute

Figure 1. The 12,500-kw 25–60-cycle 514-rpm Scherbius frequency changer set in the cold strip mill of the Jones and Laughlin Steel Corporation at Pittsburgh, Pa.

Results of Operation

Since the set was put into operation in November 1946, it has carried $28^1/_2$ per cent of the total mill load in kilowatt-hours at an average capacity factor of $46^1/_2$ per cent and a maximum capacity factor of 107 per cent in one month. During a shortage of steam for the turbine generator plant, the set was a major factor in maintaining steel production.

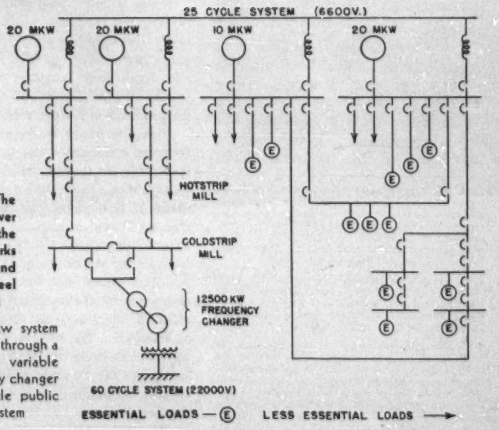

Figure 2. The 6,600-volt power system of the Pittsburgh works of the Jones and Laughlin Steel Corporation

This 70,000-kw system is connected through a 12,500-kw variable ratio frequency changer to the 60-cycle public utility system

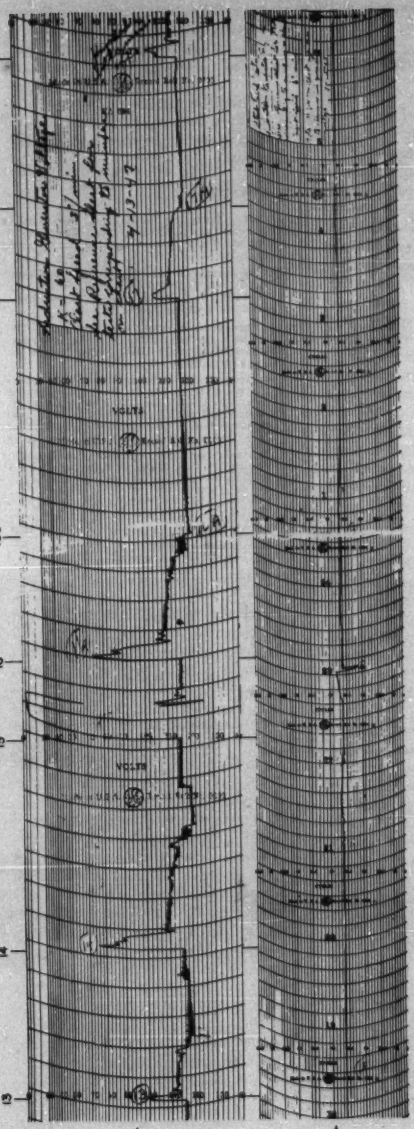

Figure 3

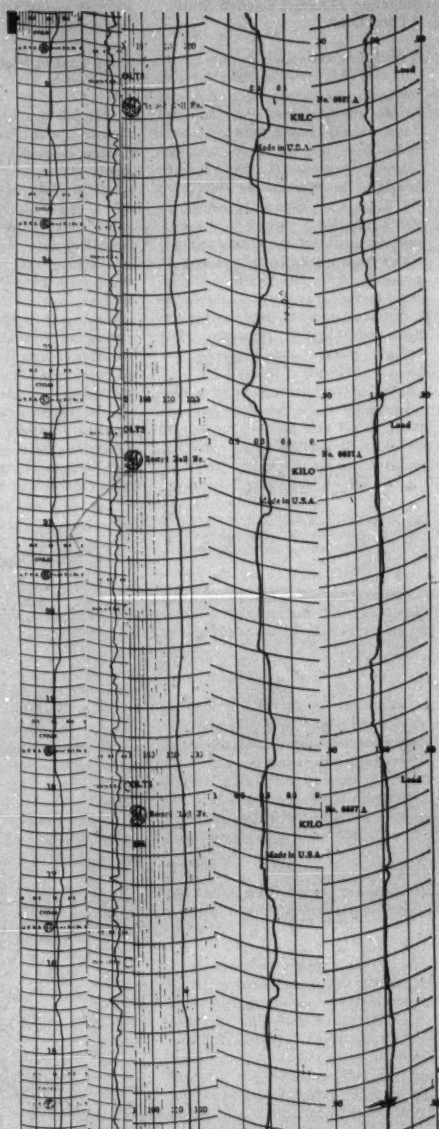

Figure 4

Induction Machine Carries Stub Feed Load

Tests have been made to indicate the ability of the induction-Scherbius portion of the set to carry stub feed load. To substantiate expectations,[2,3] two sizes of synchronous motor-generator sets were started from rest by energy from the induction generator of the frequency changer. These two motors, rated 2,200 and 3,900 kva, require approximately 10,000 and 19,000 starting kva, respectively, at rated voltage. In the test, full voltage from the induction generator at normal frequency was applied and supported by the a-c amplidyne and rheostatic-type regulators with high speed resistance and shorting contactors. The resulting voltage and frequency are shown in the charts of Figure 3.

It will be noted that the starting kilovolt-amperes of the motors is approximately 75 per cent and 140 per cent of rated kilovolt-amperes of the induction generator. So far as is known, this is the first occasion on which a stub-feed load has been started and run from an induction generator.

Load Control Maintains Constant Demand

Automatic load control of the frequency changer is such that it tends to hold a constant demand on the 60-cycle power supply and is used only under parallel operation of the frequency changer with the 25-cycle turbine generators—both sources serving a common load. Since the load regulator tends to hold a constant load through the frequency changer, variations in the load are ultimately taken by the 25-cycle turbine generators. The inherent speed regulation (less than 1 per cent slip) of the induction machine of the frequency changer is much narrower than the speed regulation (more than 4 per cent) of the 25-cycle turbine generators. The unequal regulation of these sources causes the frequency changer to increase or decrease its load as the 25-cycle system load increases or decreases. The load regulator[4,5] detects these changes and respectively causes the frequency changer to decrease or increase its load, so forcing load increases or decreases on the steam-turbine generators. These effects are shown in the high speed charts of Figure 4. Under these variable conditions of loading the regulator controls the integrated demand to a predetermined, constant value.

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No Discussion

Influence of the Core Form Upon the Iron Losses of Transformers

G. M. STEIN
MEMBER AIEE

Synopsis: The effect of the magnetic field distribution upon the iron losses of transformers is represented in practical formulas for the examples of core- and shell-type single-phase punching cores, and of the 3-phase core-type core. This computation furnishes as much as 13.5 per cent lower core losses, as compared with previous conventional methods. In another application, it is shown that a widening of the yokes permits only a relatively small reduction in losses.

FOR the computation of the iron losses in transformer cores, the designer knows the specific losses obtained under a uniform magnetization in an Epstein test. In the finished product, these values are increased by the handling and machining of the material, as well as by the stray losses in end frames and tank.[2] The losses also will rise in a magnetization directed perpendicularly to the grain orientation. The loss calculation becomes further complicated since the field is uniform only in the center part of the leg. The yokes, however, and especially the corners between yokes and legs, contain a quite heterogeneous flux distribution, as shown in Figure 1. This condition usually is taken care of by separating the core into legs and yokes, and by estimating a division of the corners between these two areas. The losses are computed for a uniform field in both parts.

This approximation has its definite limitations. If applied, for example, to the yokes, the flux density and losses appear to decrease towards zero for a very large widening, while actually the yoke losses remain a definite quantity, as shown in Figures 7 and 18 of this paper. For equal yoke and leg sections, however, the approximation becomes the product of the total core weight and of the specific losses in the leg center. The real losses, however, may be more than ten per cent smaller than this result since the corners of the yokes are only partly filled with a flux of higher density.

In order to compute the iron losses with sufficient accuracy, the actual field in the core has to be considered. Such an analysis already has been made in a previous article[3] where the results are given in the form of curves. It shall be the object of the present paper to improve the accuracy of that calculation, and to convert its curves into simple formulas for the use of the designer. For this purpose, an expression for a fictitious core weight will be developed, whose product with the specific loss in the leg center represents the total iron loss. This particular weight is different from the actual core weight, and shall, therefore, be distinguished as the "core loss weight." It is computed on the basis of the geometrical dimensions of the core, and of the specific magnetic properties of the core material.

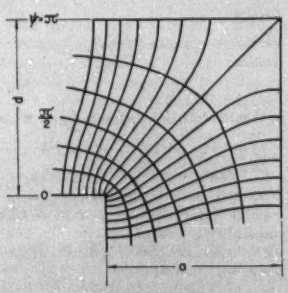

Figure 1. Field distribution in the corner of a transformer core

Derivation of the Losses From the Flux Distribution

For finding the relationship between flux distribution and core losses, introduce

L = total core losses (watts)
w = specific core losses (watts/lb.)
K, θ = material constants of core
ϕ = core flux (Maxwells)
B = magnetic flux density (kilogausses)
φ = total magnetic potential drop in core
μ = permeability of core material

The quantity w depends on B in every point of the core, according to the loss curve of the material. Each small part of this curve can be represented by the equation

$$w = (KB)^\theta \tag{1}$$

in which K and θ are material constants, and vary with B, as shown, for example, in Figure 2 for transformer sheet steel. Between $B=6$ and 15, the exponent θ is increasing in this chart from 1.6 to 2.7, and reaches an average of $\theta=2$ at $B=12$. If this value of θ is used in the analysis, the corresponding expression

$$w = K^2B^2 \tag{2}$$

for the specific iron losses w assumes the same form as the specific copper losses

$$w_c = (1/\rho)J^2 \tag{3}$$

in a conductor, Figure 3, of the resistivity ρ with the current density J. Consequently, this current flow can be compared with the magnetic field in the core if the density J corresponds to B and if the material constant $1/\rho$ is the equivalent of K^2. In addition to this, the current I and the voltage drop E of the conductor have to be replaced by the flux ϕ and by the potential drop φ in the core, as shown in Figure 4, in order to change the conductor losses

$$L_c = EI \tag{4}$$

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G. M. STEIN is design engineer with Westinghouse Electric Corporation, Sharon, Pa.

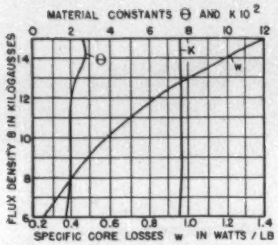

Figure 2. Loss values of transformer sheet steel for 60 cycles

into the core losses

$$L = C\phi\varphi \tag{5}$$

where C is a quantity depending on the units of ϕ and φ.

Although the permeability μ of the core varies considerably from point to point, consider μ as constant since this approximation agrees with test results.[3] The constant μ may be included in C by changing the units of φ, so that φ is coordinated to ϕ under the condition $\mu = 1$. Such a drop φ represents only a quantity introduced for loss calculations, and should not be mistaken for the magnetomotive force in the iron.

Definition of Core Loss Weight and Core Loss Length

In order to fill out the cylindrical space inside a circular coil, the transformer core usually is composed, as in Figure 5, of a number of layers of different rectangular sections (I) to (V), whose losses can be computed separately. Consequently, confine the analysis to a core with rectangular section, and start with the single-phase core-type construction, Figure 4, by introducing

a, d = width of leg and yoke sections (inches)
h = depth of core (inches)
$A = ah$ = area of leg section (square inches)
b, c = height and width of core opening (inches)
S = iron space factor
D = density of iron (pounds per inch³)
G = core weight (pounds)
G'' = core loss weight (pounds)
l = effective mean core length (inches)
l'' = core loss length (inches)
$\lambda = d/a$ = yoke widening factor

Because of μ = constant, the reluctance

$$R = \varphi/\phi \tag{6}$$

of the magnetic path inside the core becomes independent of the strength of the magnetization, and is determined only by the geometrical dimensions. Therefore, eliminate φ from the equations 5 and 6, and obtain

$$L = CR\phi^2 \tag{7}$$

From now on, let w and B describe the values in the center leg section only, and substitute the equations 2 and

$$\phi = 10^2 BA(2.54)^2 S \tag{8}$$

into equation 7, to find

$$L = \left(\frac{10^6(2.54)^4 C}{K^2} RA^2S^2\right)w \tag{9}$$

Apply this formula to a unit cube in the center of the legs, that is, replace

$$RA^2 = 1$$

and

$$L = DSw$$

in equation 9, and obtain

$$10^6(2.54)^4 C/K^2 = D/S$$

which furnishes

$$L = (DRA^2S)w \tag{10}$$

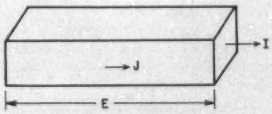

Figure 3. Schematic diagram of the current flow in a conductor

The coefficient of w depends only on the geometrical dimensions of the core, and on the density of its material, and has the dimension of a weight. Consequently, introduce that factor as the "core loss weight"

$$G'' = DRA^2S \tag{11}$$

and write

$$L = G''w \tag{12}$$

Furthermore, define a flux ψ per unit core depth by

$$\phi = h\psi \tag{13}$$

so that the equation 6 appears as

$$R = \left(\frac{\varphi}{\psi}\right)\frac{1}{h} \tag{14}$$

Since only the ratio φ/ψ enters the calculation, one component φ or ψ can become a freely chosen constant. In line with the field analysis of Appendix I, use $\psi = \pi$, that is, substitute

$$R = \frac{\varphi}{\pi}\frac{1}{h} \tag{15}$$

into equation 11, and find

$$G'' = DASa(\varphi/\pi) \tag{16}$$

The corresponding actual core weight G is derived from an effective mean core length l as[1]

$$G = DASl \tag{17}$$

By a comparison of these two equations, a corresponding fictitious core length l'' can be defined for the computation of G'', and becomes the "core loss length"

$$l'' = a(\varphi/\pi) \tag{18}$$

if equation 16 is changed into

$$G'' = DASl'' \tag{19}$$

Calculation of Core Loss Weight and Core Loss Length

By equations 16 and 18, the calculation of G'' and l'' is reduced to that of the potential drop φ around the core, whereby φ is referred to a flux $\psi = \pi$ per unit depth and to a permeability $\mu = 1$. The quantity φ shall be derived first for the single-phase core-type core since this model is the basis of other core forms.

For reasons of symmetry, only one quarter MNO_1PQO_2 of the core has to be considered, as shown in Figure 6. In this quarter core, separate the drop 1/4 φ into φ_1 of the uniformly magnetized portion of the leg and into the rest φ_2 up to the middle of the yoke.

The core region assigned to φ_1 extends up to H at a distance a from the lower edge of the yoke, since below this point the magnetic field in the leg is sufficiently uniform.[3,4] Introduce the length

$$l_1 = (1/2)b - a \tag{20}$$

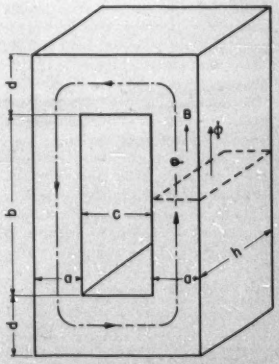

Figure 4. Schematic diagram of the magnetic circuit in a single-phase core-type core

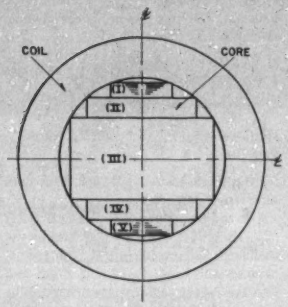

Figure 5. The rectangular components of a circular core section

of this leg area into equation 18, substitute φ_1 for φ, and obtain

$$\varphi_1 = \pi\left(\frac{b}{2a} - 1\right) \tag{21}$$

The other component φ_2 can be read from the chart, Figure 7, taken from a previous publication[3] and analyzed in Appendix I. The amounts of φ_2 are given as a scale on the center flux lines for different values of the yoke widening factor λ between $\lambda = 0.8$ and ∞. The points of equal φ_2 are connected by curves. The values of $(1/2)\ (c/a)$ are marked on the lower edge of the yoke so that φ_2 can be obtained for any given quantities of a, c, and λ.

In order to convert this chart into practical formulas, the core area is divided, as shown in Figure 6, into yoke, leg, and corner by the x, y co-ordinate system in the corner point O_1. Individual potential drops φ_n, φ_j, φ_c are assigned to these three parts, and will be computed on the basis of Figure 7.

This chart has been derived under the assumption of very long legs and yokes, and of a uniform magnetization at their center lines MN and PQ in Figure 6. Such an approximation, in general, will be satisfactory in the leg, but not in the yoke where a noticeable error is introduced.

For finding the magnitude of this error, φ_2 has been determined in Appendix I also for a finite yoke length and for the two special cases of $\lambda = \infty$ and $c = 0$. The results are plotted in Figure 18 against c/a and λ, respectively, and are compared with the corresponding amounts obtained from Figure 7 for a single corner region.

For $\lambda = \infty$, these curves reveal a constant error of 0.42 potential unit in the single corner values for the practical range between $c/a = 0.5$ and 4.0. The amount of 0.42 represents in this range the maximum possible discrepancy, since the error decreases with λ, and, as shown in Figure 18, is reduced for $c/a = 0$ from 0.70 at $\lambda = \infty$ to 0 at $\lambda = 0.75$. Consequently, the error will remain practically independent of c/a also at finite values of λ. For this reason, the correction can be made in Figure 7 at the y axis where φ_2 represents only part of the quantity φ_2 of Figure 6, and is reduced from 5.22 to 4.80 for $\lambda = \infty$, but retains its value of 4.73 for $\lambda = 0.75$; that is, a constant amount of 4.80 can be chosen for this φ_2 in the whole practical range of λ. This correction must lie also somewhere between the results obtained at the y axis from the chart, Figure 7, that is, for $c/a = \infty$, and from the calculation made for $c/a = 0$. By using the average, shown in Figure 18, find again a constant $\varphi_2 \cong 4.80$ for all desirable values of λ.

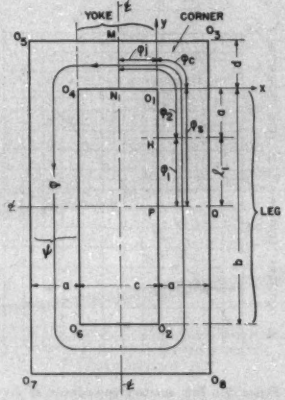

Figure 6. Sectionalization of a single-phase core-type core for loss calculations

The rest of the φ_2 in Figure 6 is the yoke drop φ_x which appears in Figure 7 between $x = 0$ and $-1/2\ c$, and which is plotted in Figure 8 against x/a replacing $-x/a$. In a uniform yoke field, find instead a potential function

$$\varphi_{ox} = (\pi/\lambda)(x/a) \tag{22}$$

For sufficiently large values of x/a, as, for example, for $x/a = 1.0$, the slope of φ_x will go over asymptotically into the direction of φ_{ox}. Consequently, approximate φ_x by its tangent line at $x/a = 1.0$ which intersects the ordinate at φ', as shown in Figure 8 for the example $\lambda = 2.0$, and write

$$\varphi_x = \varphi' + \varphi_{ox} = \varphi' + (\pi/\lambda)(x/a) \tag{23}$$

The component φ' is independent of x/a, that is, it becomes a function of λ only, which can be approximated, in the practical range between $\lambda \cong 0.8$ and 4.0, by the straight line

$$\varphi' = 0.25(\lambda - 0.9) \tag{24}$$

as shown in Figure 9.

The total drop φ_2 in Figure 6 is then composed of the drop 4.8 up to the y axis and of φ_x so that equations 23 and 24 furnish

$$\varphi_2 = \frac{\pi}{2\lambda}\frac{c}{a} + \varphi' + 4.8 \tag{25}$$

By adding φ_1 of equation 21 to this φ_2, find the total drop $1/4\varphi$ in the quarter core MNO_1PQO_3

$$1/4\varphi = \frac{\pi}{2}\frac{b}{a} + \frac{\pi}{2\lambda}\frac{c}{a} + 1.44 + 0.25\lambda \tag{26}$$

which may also be written, according to Figure 6,

$$1/4\varphi = \varphi_n + \varphi_j + \varphi_c \tag{27}$$

Consequently, define

$$\varphi_j = \frac{\pi}{2}\frac{b}{a} \tag{28}$$

as the drop in the leg portion of the quarter core for a uniform leg field, and introduce

$$\varphi_j = \frac{\pi}{2\lambda}\frac{c}{a} \tag{29}$$

as the corresponding drop in a uniform yoke field. The rest, that is,

$$\varphi_c = 1.44 + 0.25\lambda \tag{30}$$

is independent of the opening dimensions b and c and, therefore, shall be identified as the corner drop although this φ_c repre-

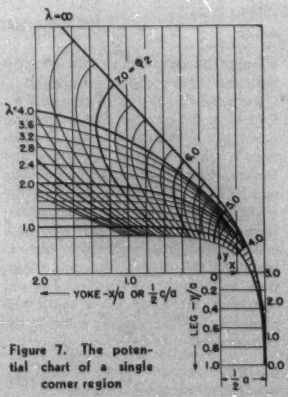

Figure 7. The potential chart of a single corner region

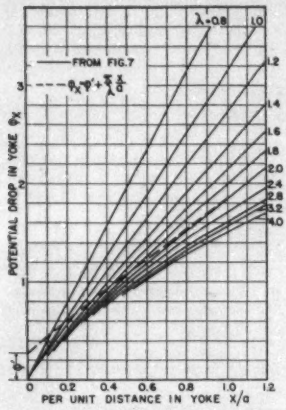

Figure 8. The potential drop in the yoke

sents more than the actual share of the corner.

This result can be checked with the field distribution derived by S. Bergmann[4] in which he considers the actual shape of the area of MNO_1PQO_2 of Figure 6. As example, he uses the extreme case of a relatively small square opening with $b/a = c/a = 0.725$ and $\lambda = 1$, and obtains $\varphi = 16.24$ as compared with $\varphi = 15.84$, computed by equation 26.

The equations 18 and 26 furnish the core loss length

$$\left.\begin{aligned} l'' &= 2[b + c/\lambda + k''a] \\ k'' &= 0.92 + 0.16\lambda \end{aligned}\right\} \text{for } \theta = 2 \tag{31}$$

of a single-phase core-type core, as compared with its effective mean length[1]

$$\left.\begin{aligned} l &= 2[b + \lambda c + ka] \\ k &= 2\lambda \end{aligned}\right\} \tag{32}$$

whereby the contribution of the corners is described by the constants k and k''.

For extending the calculation of l'' for $\theta = 2$ to any θ, consider l'' as being composed, like φ, of three separate contributions b, c/λ, and $k''a$ of legs, yokes, and corners. Since the contributions of yoke and leg have been arrived at under the condition of a uniform magnetization, they can be computed for any θ, and the equation 31 will be improved to that extent. For this purpose, let w and B continue to describe the specific losses and flux density in the leg, while w_j and $(1/\lambda)B$ represent the corresponding values in the yoke, and write, according to equation 1

$$\left.\begin{aligned} w &= (KB)^\theta \\ w_j &= (KB/\lambda)^\theta \end{aligned}\right\} \tag{33}$$

Since the actual weights G_s and G_j of legs and yokes, defined by Figure 6, are given by

$$\left.\begin{aligned} G_s &= 2AbSD \\ G_j &= 2\lambda AcSD \end{aligned}\right\} \tag{34}$$

their combined losses $L_a = G_s w + G_j w_j$ become

$$L_a = ASD2[b + (\lambda)^{1-\theta}c]w \tag{35}$$

According to a comparison with equations 12 and 19, consider

$$l_a'' = 2[b + (\lambda)^{1-\theta}c] \text{ for any } \theta \tag{36}$$

as the resultant core loss length of legs and yokes. Substitute its value in place of

$$l_a'' = 2[b + c/\lambda] \text{ for } \theta = 2 \tag{37}$$

in equation 31, and find

$$\left.\begin{aligned} l'' &= 2[b + (\lambda)^{1-\theta}c + k''a] \\ k'' &= 0.92 + 0.16\lambda \end{aligned}\right\} \tag{38}$$

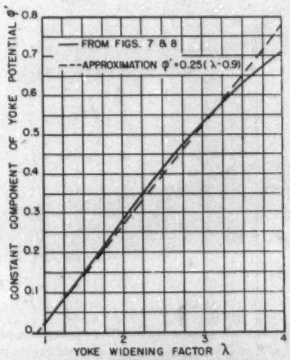

Figure 9. The constant component of the yoke potential

for the single-phase core-type core and any θ. Note that l'' becomes independent of θ for $\lambda = 1$.

On the basis of this result, l'' can be derived also for other types of punching cores.

As second example, consider, therefore, the single-phase shell-type core, Figure 10.

If this shell-type core has widened yokes, but unwidened outer legs, that is, for $d = \lambda(a/2)$ and $e = a/2$, it can be considered as composed of two core-type cores, Figure 6, of half the iron section. Consequently, the weight of the shell-type form becomes identical with that of a core-type constuction of the same opening and iron section if the corner weight is cut in half. Under this condition, derive from the equations 32 and 38

$$\left.\begin{aligned} l'' &= 2[b + (\lambda)^{1-\theta}c + k''a] \\ k'' &= 0.45 + 0.08\lambda \end{aligned}\right\} \tag{39}$$

as compared with

$$\left.\begin{aligned} l &= 2[b + \lambda c + ka] \\ k &= \lambda \end{aligned}\right\} \tag{40}$$

for the single-phase shell-type form with unwidened outer legs.

For equally widened yokes and outer legs, that is, for $d = e = a/2$, divide the shell form (Figure 10) into the center part between the lines $A-A$ and $B-B$ and into the region outside these lines. The center part has the same shape as before so that it will contribute to l'' and l half the values of equations 39 and 40. The outside region has the same widening λ in legs and yokes; that is, it has to be treated like one-half of an unwidened shell-type core of the center leg width λa. Consequently, develop

$$l'' = b + (\lambda)^{1-\theta}c + (0.8\lambda + 0.46)a + (\lambda)^{1-\theta}[b + c + (0.46 + 0.8)\lambda a]$$

and

$$l = b + \lambda c + \lambda a + \lambda[b + c + \lambda a]$$

into

$$\left.\begin{aligned} l'' &= 2\left[\frac{1 + (\lambda)^{1-\theta}}{2}b + (\lambda)^{1-\theta}c + k''a\right] \\ k'' &= 0.23 + 0.04\lambda + 0.27(\lambda)^{2-\theta} \end{aligned}\right\} \tag{41}$$

as compared with

$$\left.\begin{aligned} l &= 2\left[\frac{1 + \lambda}{2}b + \lambda c + ka\right] \\ k &= (1 + \lambda)\lambda/2 \end{aligned}\right\} \tag{42}$$

for the single-phase shell-type core with equally widened yokes and outer legs.

The 3-phase core-type core, shown in Figure 11, is magnetized in the center leg (I) and in the two U-shaped outside parts (II) and (III) like a single-phase core.

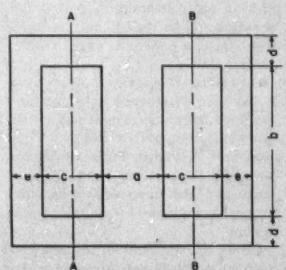

Figure 10. Schematic diagram of the single-phase shell-type core

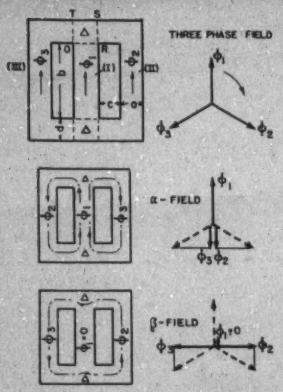

Figure 11. Schematic diagram of a 3-phase core-type core and of its single-phase flux components

In the two areas $\Delta = ORST$, the magnetization is rotating, but causes about the same losses as in a pulsating field.[5] In the special case of $\theta = 2$, these losses can be computed, according to Appendix II, as the sum of the losses in the single phase α and β fields, Figures 11 and 12.[11]

The α field of Δ is identical with the distribution above the center leg of a shell-type core of the yoke widening 2λ so that the drop φ_α in each Δ is furnished by equations 30 and 72 as

$$\varphi_\alpha = \varphi_c/2 + 0.72 + 0.25\lambda \tag{43}$$

The corresponding drop φ_β in each Δ is derived in Appendix II in terms of a drop φ_u over a distance a in a uniform β field of the yoke. φ_u is obtained from φ_f in equation 29 by interchanging $c/2$ with a. Since the flux in the β field is only 86.6 per cent of the 3-phase flux, φ_u must be reduced also by the ratio of the corresponding losses. Consequently, substitute

$$\varphi_u = 0.75\pi/\lambda \tag{44}$$

into equations 74 and 75, and obtain

$$\varphi_\beta = \varphi_u(1-\sigma) = 2.36(1-0.15\lambda)/\lambda \tag{45}$$

According to equations 43 and 45, each Δ space will contribute a resultant drop

$$\varphi_\alpha + \varphi_\beta = \begin{Bmatrix} 0.72 + 0.25\lambda + 2.36/\lambda - 0.35/\lambda^2 \\ 2.98 = 0.95\pi \text{ for } \lambda = 1 \end{Bmatrix} \tag{46}$$

to the total drop in the 3-phase core, that is, for $\lambda = 1$, this contribution is 5 per cent lower than the corresponding drop π in a uniform field carrying the full leg flux. This difference becomes even smaller for higher λ, and will cause an error of less than 1 per cent in the total losses since the Δ space represents in the average only 10 per cent of the whole core. Consequently, assume a uniform Δ field of the same size as in the yokes, and extend its loss calculation to any θ.

The value of l'' of the 3-phase core, Figure 11, then will be equal to that of a single-phase core-type core, Figure 6, of the same opening and core section, plus the core loss length of one leg, two yokes,

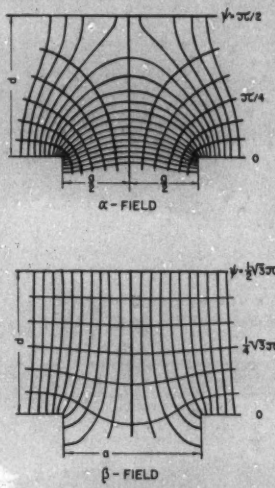

Figure 12. The single-phase flux components in the Δ space of a 3-phase core

and the two regions Δ. Consequently, derive from equation 38

$$\left.\begin{aligned} l'' &= 3[b + 1.33(\lambda)^{1-\theta}c + k''a] \\ k'' &= 0.61 + 0.11\lambda + 0.67(\lambda)^{1-\theta} \end{aligned}\right\} \tag{47}$$

as compared with

$$\left.\begin{aligned} l &= 3[b + 1.33\lambda c + ka] \\ k &= 2\lambda \end{aligned}\right\} \tag{48}$$

for the 3-phase core-type core.

Numerical Applications

In a comparison of the foregoing loss calculation with present conventional methods, use first the example of a 100-kva single-phase shell-type core, Figure 10. Its numerical data are $a = 5^3/_8$; $h = 7^{15}/_{16}$; $b = 12^1/_4$; $c = 4^3/_{16}$; $\lambda = 1$; $D = 0.271$; $S = 0.89$. Because of the unwidened yoke, any conventional method will furnish L as the product of G and w, as compared with the product of G'' and w, introduced in equation 12 for a loss calculation by flux distribution; or the ratio of the losses obtained by the old and new methods will be equal to the ratio of G to G''. Consequently, compute from equations 17, 19, 39, 40 the values $l'' = 38.23$; $G'' = 392$ in relation to $l = 43.38$; $G = 445 = 1.135\ G''$; that is, the conventional way leads to 13.5 per cent higher losses than derived from the actual flux distribution. If such a conventional calculation is compared with test results, some of the stray losses in end frames, tank, and other sources will be considered erroneously as part of the core losses, and will remain undetected.

As a second example, the problem of how much L can be decreased with an increase of λ already has been solved for the single- and 3-phase cores, Figures 4 and 11, and for $a = c$ and $b = 5c$ in a previous publication.[5] In this paper, the losses are computed on the basis of the actual flux distribution, as given by the chart Figure 7, and also in a conventional way for a uniform field where the core is divided into legs and yokes by the x axis, Figure 6. The results are cited in Figures 13 and 14, where the per cent decrease ΔL of the

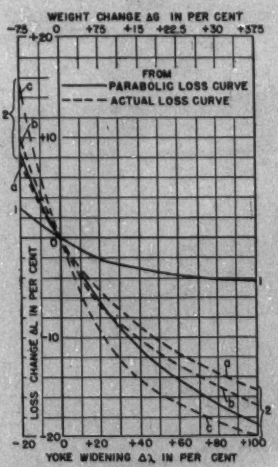

Figure 13. Loss change with yoke widening in a single-phase core-type core

1—From chart, Figure 7
2—Conventional method

(a)—B = 10 kilogausses
(b)—B = 12 kilogausses
(c)—B = 15 Kilogausses

losses is plotted against the per cent increase $\Delta\lambda$ and ΔG of λ and G.

In the single-phase example, Figure 13, and for $\Delta\lambda = 40$ per cent or $\Delta G = 15$ per cent, the field distribution furnishes a loss saving of 3 per cent, while the conventional method indicates 10 per cent or more reduction in losses, depending on the flux density in the legs.

In the 3-phase example, Figure 14, the calculation has been checked by an experiment. For this purpose, the originally high yokes of a small 3-phase core of rectangular iron section were reduced step by step and the losses measured before and after each cutting operation. These test values of ΔL change very little with the flux density in the legs, and agree quite closely with those derived from the field distribution. This time a yoke increase of $\Delta\lambda = 40$ per cent with $\Delta G = 16$ per cent, effects an actual decrease in losses of 6 per cent, as compared with 9 to 15 per cent obtained in the conventional way.

From the first example, conclude that the actual losses are, for $\lambda = 1$, considerably smaller than the result of present methods. According to Figures 13 and 14, this discrepancy will decrease if λ is raised; that is, a widening of the yokes reduces the error inherent in conventional calculations.

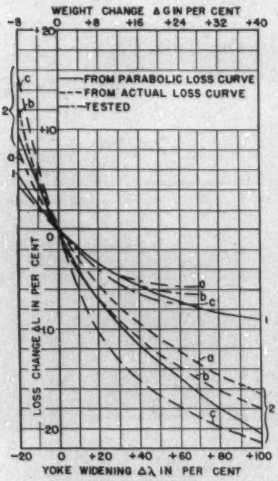

Figure 14. Loss change with yoke widening in a 3-phase core-type core

1—From chart, Figure 7
2—Conventional method
(a)—$B = 10$ kilogausses
(b)—$B = 12$ kilogausses
(c)—$B = 15$ kilogausses

Conclusions

By the use of loss formulas, derived from the actual field distribution, the designer is able to make a reliable investigation of the economy of any core construction. Not only can the shape of the core be chosen to obtain optimum performance with a minimum cost, but it also will be possible to detect additional stray losses so that they can be reduced. The equations may further form the basis of an analytical design of a new line of transformers.

Appendix I. The Calculation of the Field Distribution

For the calculation of the field in the core Figure 6, the corner region at $O_1\,O_2$ is approximated by an L shaped space A in which the other corners at $O_3\,O_4$, $O_4\,O_5$, and $O_6\,O_7$ are moved to infinity. As shown in Figure 15, this space A represents a polygon $O_1\,O_2\,O_3\,O_4$ which lies in a Z plane ($Z = x + jy$; $j = \sqrt{-1}$). This polygon A is mapped conformally upon a strip A of the width π in a χ plane ($\chi = \varphi + j\psi$) where the distribution of the potential φ and of the flux ψ becomes a simple parallel field. This transformation is made in two steps by referring both regions A to an upper half W plane ($W = u + jv$) so that the iron boundary in the Z plane corresponds to the real u axis in the W plane and to the strip boundary in the x plane.

Since any three points can be co-ordinated freely on these boundaries, choose

Point	Plane Z	Plane W	Plane X
O_1	0	$+1$	0
O_2	$-j\infty$	∞	$+\infty$
O_3	$a + jd$	$-p$	$-m + jx$
O_4	$-\infty$	0	$-\infty$

where p and m represent constants to be determined.

The W and χ planes are mapped upon each other by the function[6]

$$W = \epsilon^{\chi} \quad (\epsilon = 2.7183) \tag{49}$$

According to Schwarz and Christoffel, the polygon transformation from the Z plane upon the upper half W plane is performed uniquely by a function whose derivative represents the given difference of directions of the two boundaries, and is, therefore, subjected to the prescribed changes at the vertices. In order to obtain a general form of this derivative, introduce

γ_1 to γ_4 = the supplements of the polygon angles at O_1 to O_4 in the Z plane
u_1 to u_4 = the co-ordinates of O_1 to O_4 in the W plane
C = an unknown constant

and write

$$\frac{dZ}{dW} = \frac{C}{(W-u_1)^{\gamma_1/\pi}(W-u_2)^{\gamma_2/\pi} \times (W-u_3)^{\gamma_3/\pi}(W-u_4)^{\gamma_4/\pi}} \tag{50}$$

In the special case, Figure 15, that is, for $\gamma_1 = -\pi/2$, $\gamma_2 = \gamma_4 = +\pi$, $\gamma_3 = +\pi/2$, and for $u_1 = +1$, $u_2 = \infty$; $u_3 = -p$; $u_4 = 0$, the factor containing u_2 disappears because of $u_2 = \infty$. Consequently find

$$\frac{dZ}{dW} = \frac{C}{W}\sqrt{\frac{W-1}{W+p}} \tag{51}$$

For computing C and p, develop this expression in the neighborhood of O_2 and O_4, where it will go over into the transformation of a strip region in the Z plane upon the upper half W plane, that is, the function becomes here identical with equation 49 for the mapping of the χ plane upon the W plane, with the exception of the strip width and of the direction of the boundaries. Consequently, write

At O_2; that is, $W \to \infty$;
$Z = C \log W = -j(a/\pi) \log W$

At O_4; that is, $W \to 0$;

$$Z = C\frac{j}{\sqrt{p}} \log W = (d/\pi) \log W$$

introduce the co-ordinates of O_3 into equation 49, and find

$$C = -j(a/\pi); \quad p = (a/d)^2 = (1/\lambda)^2$$
$$m = 2 \log \lambda \tag{52}$$

so that the equation 51 furnishes

$$Z = -j(a/\pi)\int_{+1}^{W} \frac{dW}{W}\sqrt{\frac{W-1}{W+(1/\lambda)^2}} \tag{53}$$

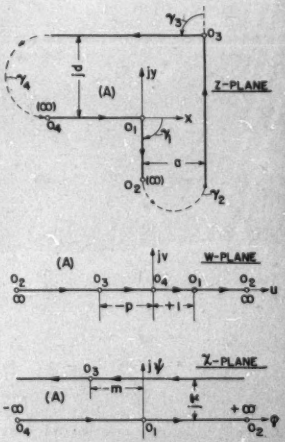

Figure 15. The transformation of a single corner region

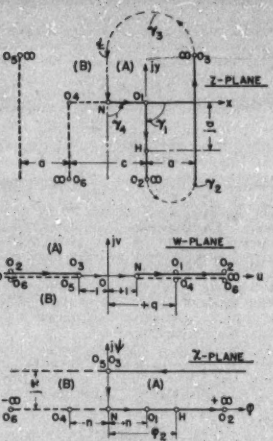

Figure 16. The transformation of an upper core region with infinite yoke height

$(\lambda = d/a = \infty)$

Substitute equation 49 into the solution of this integral, and obtain

$$Z = -j\frac{2}{\pi}\left\{a \tanh^{-1}\sqrt{\frac{\epsilon^x-1}{\epsilon^x+(1/\lambda)^2}} - d \tanh^{-1}\left[\frac{1}{\lambda}\sqrt{\frac{\epsilon^x-1}{\epsilon^x+(1/\lambda)^2}}\right]\right\} \quad (54)$$

For the separation into real and imaginary components, combine

$$re^{j\delta} = \sqrt{\frac{\epsilon^x-1}{\epsilon^x+(1/\lambda)^2}}$$

find

$$\left.\begin{aligned} r &= \sqrt{\lambda}\left\{\frac{\cosh\varphi - \cos\psi}{\cosh(\varphi+m)+\cos\psi}\right\}^{1/4} \\ \delta &= \frac{1}{2}\left\{\tan^{-1}\frac{\sin\psi}{\cos\psi - \epsilon^{-\varphi}} - \tan^{-1}\frac{\sin\psi}{\cos\psi + \epsilon^{-(\varphi+m)}}\right\} \end{aligned}\right\} \quad (55)$$

and derive

$$\left.\begin{aligned} x &= \frac{1}{\pi}\left\{a \tan^{-1}\frac{2r\sin\delta}{1-r^2} - d \tanh^{-1}\frac{2(r/\lambda)\sin\delta}{1+(r/\lambda)^2}\right\} \\ y &= \frac{1}{\pi}\left\{d \tan^{-1}\frac{2(r/\lambda)\cos\delta}{1-(r/\lambda)^2} - a \tanh^{-1}\frac{2r\cos\delta}{1+r^2}\right\} \end{aligned}\right\} \quad (56)$$

as basis of the field distribution Figure 1, and of the α field, Figure 12.

The flux density in the core, Figure 6, becomes infinite at O_4 and zero at O_5. If now O_4 and O_5 are moved into the infinity while M and N are retained at $x = -c/2$, the density and the potential will be raised at M, but lowered at N, so that somewhere on MN the potential must remain the same. Approximate the location of such a point by the intersection of MN with the center flux line $\psi = \pi/2$, and introduce the corresponding values of r and δ, derived from equation 55,

$$\left.\begin{aligned} r &= \sqrt{\lambda}\left\{\frac{\cosh\varphi}{\cosh(\varphi+m)}\right\}^{1/4} \\ \delta &= \frac{1}{2}\left\{\pi - \tan^{-1}(\epsilon^{\varphi}) - \tan^{-1}(\lambda^2\epsilon^{\varphi})\right\} \end{aligned}\right\} \quad (57)$$

for $\psi = \pi/2$ into equation 56 to obtain the co-ordinates x and y of the curves, Figure 7, and the scales of

$$\varphi_2 = \varphi(y = -ja) - \varphi(x = -c/2)$$

For checking the accuracy of this approximation, analyze also two models of the whole top half of a core in which the corners at O_5O_7 and O_7O_8 are moved to infinity. In the first case, Figure 16, make $\lambda = \infty$, and choose $c = 0$ for the second form, Figure 17.

In Figure 16, analyze, for reasons of symmetry, only the right half of the core, that is, the region A which becomes a polygon $O_1O_2O_3N$ in a Z plane. By way of an upper half W plane, this polygon is mapped on the right half strip region A in a χ plane with a parallel field of φ and ψ, by choosing the following location of boundary points:

Point	Plane Z	Plane W	Plane χ
O_1	0	$+q$	$+n$
O_3	$-j\infty$	∞	$+\infty$
O_2	$a+j\infty$	-1	$+j\pi$
N	$-c/2$	$+1$	0

if q and n are unknown constants. Simultaneously, the lower half B of the core in the Z plane becomes the lower W plane and the left half strip of the χ plane.

For the co-ordination between the W and χ planes, write

$$W = \cosh\chi \quad (58)$$

and find the polygon transformation from the Z plane upon the W plane by the principle of Schwarz and Christoffel; that is, introduce $\gamma_1 = -\pi/2$, $\gamma_3 = \gamma_5 = +\pi$, $\gamma_4 = +\pi/2$, and $u_1 = +q$, $u_3 = \infty$, $u_5 = -1$, $u_4 = +1$ into equation 50, and derive

$$\frac{dZ}{dW} = C\frac{1}{W+1}\sqrt{\frac{W-q}{W-1}} \quad (59)$$

For computing C and q, develop this function in the neighborhood of O_3 and O_5 where it will go over into the mapping of a simple strip upon an upper half plane, as described by the equation 49. Consequently, write

At O_3; that is, $W \to \infty$;
$Z = C\log(W+1) = -j(a/\pi)\log(W+1)$

At O_5; that is, $W \to -1$;

$$Z = C\sqrt{\frac{1+q}{2}}\log(W+1) = -j\frac{a+c/2}{\pi}\log(W+1)$$

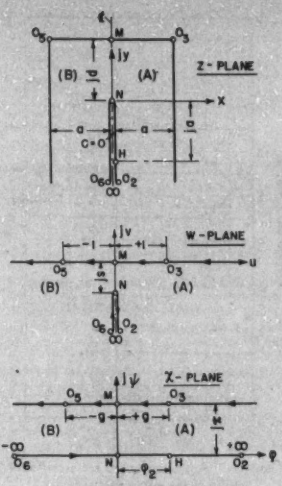

Figure 17. The transformation of an upper core region with zero opening width

$(c = 0)$

introduce the co-ordinates of O_1 into equation 58, and find

$$\left.\begin{aligned} C &= -j(a/\pi) \\ \cosh n &= q = 2\left(1+\frac{c}{2a}\right)^2 - 1 \end{aligned}\right\} \quad (60)$$

so that equation 59 furnishes

$$Z = -j(a/\pi)\int_{+q}^{W}\frac{dW}{W+1}\sqrt{\frac{W-\cosh n}{W-1}} \quad (61)$$

Substitute equation 58 into the solution of this integral, and derive

$$\left.\begin{aligned} Z &= -j\frac{2a}{\pi}\left\{\tanh^{-1}\sqrt{\frac{\cosh\chi - \cosh n}{\cosh\chi - 1}} - \cosh\frac{n}{2}\tanh^{-1}\left[\frac{1}{\cosh\frac{n}{2}}\sqrt{\frac{\cosh\chi - \cosh n}{\cosh\chi - 1}}\right]\right\} \end{aligned}\right\} \quad (62)$$

For $Z = -ja$ the variable χ becomes equal to the drop φ_2 between N and H, Figure 6, so that

$$\frac{\pi}{2} = \tanh^{-1}\sqrt{\frac{\cosh\varphi_2 - \cosh n}{\cosh\varphi_2 - 1}} - \cosh\frac{n}{2}\tanh^{-1}\left[\frac{1}{\cosh\frac{n}{2}}\sqrt{\frac{\cosh\varphi_2 - \cosh n}{\cosh\varphi_2 - 1}}\right] \quad (63)$$

has to be solved for φ_2. Because of

$$0.988 < \sqrt{\frac{\cosh\varphi_2 - \cosh n}{\cosh\varphi_2 - 1}} < 1.00$$

for $10 > c/a > 0.1$, the root in the second term

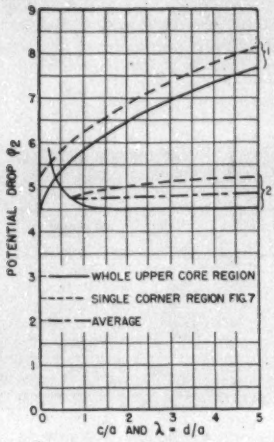

Figure 18. Approximate and actual potential drops in the upper core region

1—Plotted against c/a for $\lambda = d/a = \infty$
2—Plotted against $\lambda = d/a$ for $c/a = 0$

of equation 63 can be approximated by unity, and furnishes, together with equation 60,

$$\left.\begin{aligned} Q &= \pi/2 + \cosh\frac{\pi}{2}\operatorname{cnth}^{-1}\left(\cosh\frac{\pi}{2}\right) \\ \varphi_2 &= \cosh^{-1}\left[1 + (\cosh\pi - 1)\cosh^2 Q\right] \\ \cosh\pi &= 1 + \frac{c}{2a} \\ \cosh\pi - 1 &= \frac{c}{a}\left(2 + \frac{c}{2a}\right) \end{aligned}\right\} \quad (64)$$

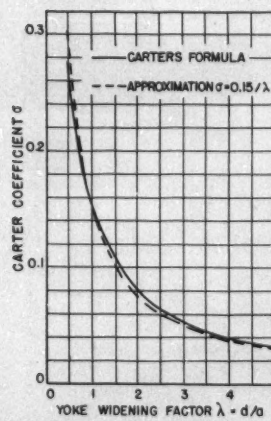

Figure 19. The Carter coefficient

These value of φ_2 are plotted against c/a in Figure 18, where they are compared with the result of Figure 7.

The second model, Figure 17, can be interpreted as a half strip which is slit between N and infinity in a Z plane. According to a suggestion of F. Roeding, the field of this strip is transformed into the parallel flux distribution of a simple strip in an χ plane by using a lower half slit W plane as parameter with the following co-ordination of boundary points:

Point	Plane Z	Plane W	Plane χ
N	0	$-js$	0
O_2	$-j\infty$	$-j\infty$	$+\infty$
O_1	$a+jd$	$+1$	$g+j\pi$
M	$+jd$	0	$+j\pi$
O_3	$-a+jd$	-1	$-g+j\pi$
O_4	$-j\infty$	$-j\infty$	$-\infty$

where g and s are unknown constants.

As can be recognized directly, this transformation is described by

$$\left.\begin{aligned} W &= \sin\left[\frac{\pi}{2a}(Z-d)\right] \\ W &= -js\cosh\frac{\chi}{2} \end{aligned}\right\} \quad (65)$$

These functions may also be derived from the principle of Schwarz and Christoffel,[4–11] if the regions A and B in the three planes are interpreted as polygons. By substituting the co-ordinates of N, obtain

$$\left.\begin{aligned} s &= \sinh(\lambda\pi/2) \\ g &= 2\sin^{-1}\left[\frac{1}{\sinh(\lambda\pi/2)}\right] \end{aligned}\right\} \quad (66)$$

and

$$\cosh\frac{\chi}{2} = j\frac{\sin\left[\frac{\pi}{2a}(Z-jd)\right]}{\sinh(\lambda\pi/2)} \quad (67)$$

Because of $\chi = \varphi_2$ for $Z = -ja$, derive

$$\varphi_2 = 2\cosh^{-1}\left[\frac{\sinh[(1+\lambda)\pi/2]}{\sinh(\lambda\pi/2)}\right] \quad (68)$$

which is plotted against λ in Figure 18, as compared with the result of Figure 7.

Appendix II. The Field Distribution in a 3-Phase Core

For each moment t, the distribution of the fluxes ϕ_1, ϕ_2, ϕ_3 in a 3-phase core type core, Figure 11, can be linearly composed of two single-phase excitations, displaced by 90 electrical degrees and named the α and β fields.[2,11] The peak values B_α of the flux density in the α field are identical with the momentary 3-phase values B_t at the instance when ϕ_1 in the center leg reaches its maximum. The corresponding peak values B_β of the β field are given by the distribution of B_t at the time when ϕ_1 becomes zero. At a frequency f and for $\omega = 2\pi f$, obtain, therefore,

$$B_t = B_\alpha\cos\omega t + B_\beta\sin\omega t \quad (69)$$

in every core point.

The two components of B_t have the same geometrical direction in the areas I, II, and III, Figure 11, so that the peak value B of B_t becomes here

$$B^2 = B_\alpha^2 + B_\beta^2 \quad (70)$$

In the special case $\theta = 2$, substitute the corresponding specific losses w, w_α, and w_β, according to equation 2, find

$$w = w_\alpha + w_\beta \quad (71)$$

and obtain the same equation for the total losses. Consequently, the total 3-phase losses of (I), (II), and (III) could be described by the sum of the single-phase potentials in the α and β fields. Instead of this, derive these 3-phase losses directly from the resultant field since it has in those three areas the same shape and pulsating character as in a single phase core.

In the regions Δ, Figure 11, however, the two components of B_t have different directions, and are to be added as geometrical vectors in equation 69. The resultant vector B_t is moving on an ellipse,[2,5] and represents, therefore, a rotating magnetization. According to test results,[2,5] the corresponding losses are approximately the same as in a pulsating field, and become for $\theta = 2$ the sum of the losses in the α and β fields, as in the rest of the core.

The distribution of the α and β fields in Δ is shown in Figure 12.[2,5] Consequently, the α field is identical with the field above the center leg of a single-phase shell-type core, Figure 10; that is, the corresponding drop φ_u in each Δ becomes the corner drop for a flux $\psi = \pi/2$ in a core type core, Figure 6, and is given by

$$\varphi_u = (1/2)\varphi_c \quad (72)$$

if φ_c is referred to $\psi = \pi$.

The β field has in Δ the same shape as the magnetic field in the gap between a slotted and smooth surface of a dynamo machine with a gap length d and a slot width a. Each slot is causing a loss of σa in flux carrying circumference of the rotor. The coefficient

$$\sigma = \frac{2}{\pi}\left\{\cot^{-1}(2\lambda) - 2\lambda\log\sqrt{1+\left(\frac{1}{2\lambda}\right)^2}\right\} \quad (73)$$

has been found by F. W. Carter,[7,12,13] and is plotted against $\lambda = d/a$ in Figure 19 where it also is approximated by the hyperbola

$$\sigma \cong 0.15/\lambda \quad (74)$$

The potential drop φ_β in each Δ space, Figure 11, corresponds to the gap flux above a slot, and is reduced in the same scale. Consequently, find

$$\varphi_\beta = (1-\sigma)\varphi_u \quad (75)$$

where φ_u represents the drop over a length a in a uniform β field of the yoke.

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Discussion

T. D. Gordy (General Electric Company, Pittsfield, Mass.): The author has attacked the problem of calculating core loss in transformers by mathematically determining the flux distribution in the core, obtaining effective core-loss producing weight, then obtaining total core loss from effective weight and specific core loss. This is a mathematical physics approach based upon assumptions that make the problem mathematically solvable.

Hot-rolled sheet steels that have been used in transformer cores for many years do not have variable permeability and pronounced directional effects to the degree that modern cold-rolled highly oriented steels have, and, possibly, by assuming constant permeability and no directional effects, the proposed method of calculation would be justified for hot-rolled sheet steel. However, cold-rolled strip steels have permeabilities that are not only functions of flux density, but also functions of the direction in which flux passes through the strip with respect to its rolling direction. Figure 1 of this discussion shows these variations for a cold-rolled steel used in transformer cores for with-rolling direction, 55 degrees from with-rolling direction, and across rolling direction. These curves do not appear to justify the assumption of constant permeability.

Specific core loss is the loss in the center of the leg section when the lines of flux are uniformly distributed and parallel to the rolling direction. However, in the core, and particularly in the corner sections, flux passes through the core steel at directions other than with the rolling direction. Core loss varies considerably with the direction of flux passing through the steel as well as with flux density. Figure 2 shows these variations for a representative cold-rolled strip steel used in transformer cores.

Considering the effect of variable permeability and directional effects, it is questionable whether or not the analysis presented would yield more accurate results than older methods of core loss calculation. If, however, the author's analysis be modified to account for variable permeability and directional effects, calculated flux distributions and core losses may be verified by tests. These modifications result in introducing harmonics in the flux, especially in corner regions, which in turn affect core loss. The concept of an effective core loss producing weight still may be applied with the modifications mentioned.

Stein's analysis does not account for the effects of joints on flux distribution, core loss, and exciting current. It has been determined that the type of joint affects the flux distribution in the core, total core loss, and exciting current. Figure 3 illustrates the variations of core loss and exciting current with flux density for two types of joints in transformer cores built of modern core steel.

The analysis presented by Stein is a good example of the application of mathematical physics to a rather complex engineering problem with certain simplifying assumptions, and it serves as a guide in the treatment of the problem. However, the method as presented in the paper is not sufficiently complete to be applicable to actual core designs utilizing modern core steels because of variable permeability, directional effects, and joint effects.

G. M. Stein: Gordy proposes to extend the analysis of the paper to the application of cold-rolled steel and to the losses caused by the joints.

It is true that the formulas derived apply only to hot-rolled steel. Cold-rolled material introduces an additional variable since its permeability and losses possess a higher sensitivity to the angle between the field and the grain orientation. As function of the flux density, however, the permeability curve of cold-rolled steel has in Figure 4 of this discussion about the same shape as the curve of hot-rolled steel. The difference in slope at the higher flux densities beyond the point of maximum permeability does not appear large enough to have a profound effect on the flux distribution. In addition to this, the variation of the permeability of cold-rolled steel with the grain orientation is, according to Figure 1 of Gordy's discussion, not worse than the change with the flux density. Consequently, it should be permissible to calculate the flux distribution in cold-rolled steel, like in hot-rolled steel, on the assumption of a constant permeability

The influence of the grain orientation, however, cannot be neglected in an analysis of the core losses for cold-rolled material, but on the basis of a constant permeability the loss formulas may be corrected for this property, as demonstrated hereinafter, since the angles between the flux density and the grain can be derived from the field equations.

A joint may have a 2-way effect upon the losses. First, consider the losses in the joint

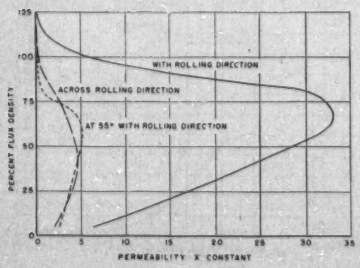


Figure 1 (left). Variation of permeability with flux density for a modernt ransformer core steel

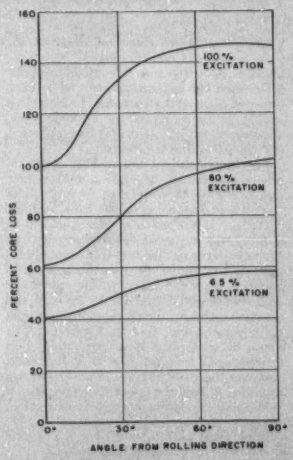
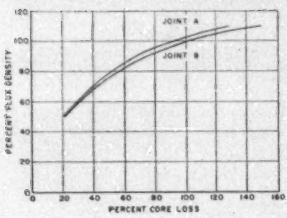


Figure 2. Variation of core loss with angle from rolling direction for a modern transformer core steel



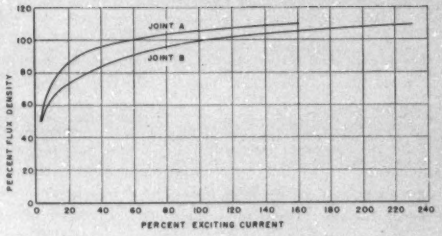


Figure 3. Variation of core loss and exciting current with flux density for two types of joints in transformer cores built of modern core steel

A (left). Flux density versus core loss

B. Flux density versus exciting current

itself which are caused by the passage of the flux from one lamination to another. These joint losses are practically independent of the location of the joints in the core, that is, they represent additional losses[1] which lie outside the scope of the paper. In cold-rolled steel also the location of the joints will have an influence upon the losses since the angles between field and grain depend on this location.

In order to illustrate the method by which the losses in cold-rolled steel can be computed on the basis of the flux distribution, consider the example of a core-type transformer with a yoke widening factor $\lambda=1$, whose corner field is shown in Figure 1 of the paper. Let B_x and B_y represent the components of a resultant flux density B_r in each core point and in the x, y, co-ordinate system, Figure 6 of the paper. B_r becomes equal to B in the uniformly magnetized leg center. Then derive from the field function $X=\varphi+i\psi$ of the potential φ and flux ψ by equations 49, 51, and 52 of the paper

$$\left.\begin{aligned} B_x-jB_y &= -\frac{a}{\pi}B\frac{dx}{dZ}=jB\sqrt{\frac{e^X+1/\lambda^2}{e^X-1}} \\ B_x &= \frac{B}{r}\sin\delta \\ B_y &= -\frac{B}{r}\cos\delta \\ B_r &= \frac{B}{r} \end{aligned}\right\} \quad (1)$$

where r and δ are given by equation 55. Note $\varphi=0$ at the corner point O_1. The components b_xB_x/B_r and $B_y=B_y/B_r$ already have been derived as functions of φ in a paper of Stein and Uhlmann[3] in which they appear as Figure 24 for the example $\lambda=1$. The corresponding values of B/B_r are plotted in Figure 5 of this discussion.

For computing the influence of the grain orientation, use the conditions on the center flux line $\psi=\pi/2$ as average for the whole core, where B_r assumes for $\lambda=1$ the constant value B. If the material is magnetized in parallel with the grain in the leg center and if the core consists of full frame punchings without any joints, the quantity δ, given in equation 55 of the paper, will represent the angle between the direction of the field and the grain, and is, therefore, plotted against φ for $\psi=\pi/2$ and $\lambda=1$ in Figure 6. The losses of cold-rolled steel are obtained as functions of δ from test results, so that their rise over the straight grain losses ($\delta=0$) can be added in Figure 6 also as function of φ.

Without considering the grain orientation, the core loss length l^v is given as

$$l^v=(a/\pi)\varphi$$

(equation 18 of paper). This formula also may be written

$$l^v=(a/\pi)\int d\varphi \quad (2)$$

where the integration over φ can be made on the center flux line, and where the differential $d\varphi$ is a measure for the losses in each cross section of the core. Under the influence of the grain orientation, this differential is increased by a factor $1+\tau$ in which τ as a function of φ is given by the iron loss rise, shown in Figure 6; that is, write instead of equation 2

$$l^v=(a/\pi)\int(1+\tau)d\varphi=(a/\pi)(\varphi+\int\tau d\varphi) \quad (3)$$

The integral

$$\varphi_a=\int\tau d\varphi \quad (4)$$

is proportional to the area between the loss curve Figure 6, the abscissa, and the joints wherever they are located, and therefore, will be a measure for the total loss increase caused in each quarter core by the influence of the grain orientation. Such a quantity

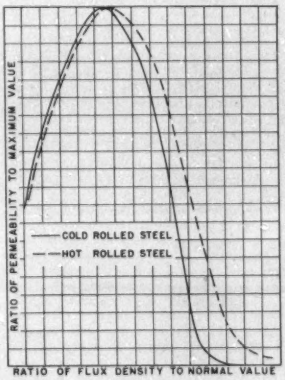


Figure 4 (above). Permeability curves of hot-rolled and cold-rolled steel

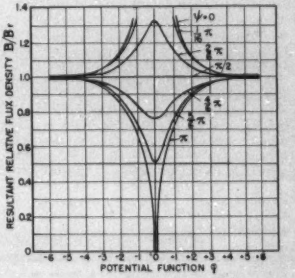


Figure 5. Distribution of the resultant flux density in a single corner without yoke widening

($\lambda=1$)

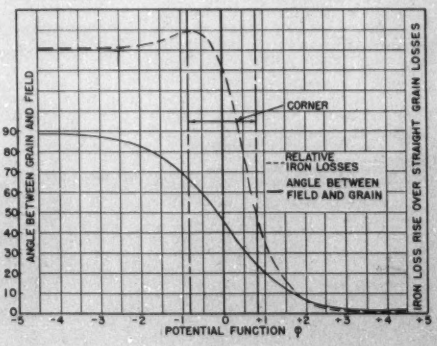


Figure 6 (right). The angles between field and grain and the losses of cold-rolled steel at the center flux line of a single corner without yoke widening

($\lambda=1$)

φ_a can be derived in the same way also for all other values of λ, and is practically independent of the length of legs and yokes. Add φ_a to the total core potential given in equation 26 of the paper, and obtain a potential

$$(1/4)\varphi = \frac{\pi b}{2a} + \frac{\pi c}{2a} + 1.44 + 0.25\lambda + \varphi_a \qquad (5)$$

which includes the influence of the grain orientation. Herewith derive in the same way as in equation 38 of the paper

$$\left.\begin{aligned} l'' &= 2[b + (\lambda)^{1-\theta} c + k'' a] \\ k'' &= 0.92 + 0.16\lambda + \frac{2}{\pi}\varphi_a \end{aligned}\right\} \qquad (6)$$

for the single-phase core-type core of cold-rolled steel. The core losses computed by this equation have been found to agree well with test results for which a core-type core has been assembled out of Epstein samples.

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1. Reference 2 of the paper, Figures 3 and 4.
2. Reference 5 of the paper.

Some Economic Aspects of Radio-Frequency Heating

LAWRENCE M. DURYEE
MEMBER AIEE

THE IMPORTANCE of the industrial and commercial field of high frequency heating is illustrated by the following proposed statement of the working group on definitions of the AIEE subcommittee on electronic heating:

Since the installed capacity of these equipments is approaching half a million kilowatts with a total value of between 40 and 50 million dollars, and inasmuch as the volume of business has doubled in each 4-year period since the inception of this type of heating 30 years ago, and since the annual business at present is between 6 and 8 million dollars a year, the importance of this development cannot be overemphasized.

For several years, those in the industry have stressed, in the technical press, the many advantages accruing to users of high frequency heating, together with the technical problems involved. The time now has arrived when these advantages and technical problems must be approached from an economic angle, clarified, authenticated, and simplified by actual analysis and cost data in specific industries. This goal, combined with a study of other important aspects of the subject of high frequency heating, is the purpose of this paper.

Definition of Frequencies

The terms now used to define the general bands of frequencies are in need of revision, due to recent rapid developments. The term "high frequency" in 1925 applied to the 500-cycle coreless-type melting furnaces. Five years ago this same term applied to the 500-kc band used for induction heating. Then followed the vague term "ultra high frequency" employed to describe the band up to and including 30 megacycles in the dielectric field of heating. Now we have developed "microwave heating" in the 3,000-megacycle band. And tomorrow?

So confused have these terms become in this entire area that provision should now be made for fitting all the frequencies of the electromagnetic spectrum into an orderly sequence. This solution, recently discussed and tentatively adopted by the subcommittee on electronic heating, is submitted in Table I.

This proposal is included here simply because any economic study of a problem obviously must have its very beginning in an economy of words and in simplification of terms.

Economics of Generator or Oscillator Selection—Induction

If a generator (or oscillator) feeds a receiving circuit containing both resistance R and inductive reactance X_L, the source must have sufficient kilovolt-ampere capacity, of course, to supply both the power component (kilowatt) and the reactive component (kilovar) of the load. If the reactive component is supplied by shunt capacitors (X_C) then the generator kilovolt-ampere and kilowatt ratings are the same.

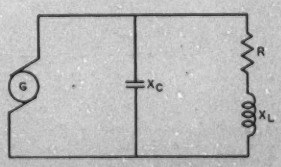

Figure 1. Circuit containing resistance and inductance in series, with shunt capacitor

Assuming the cost of the generator, in this latter case, is proportional to its kilovolt-ampere rating, then its cost also will be proportional to its kilowatt power output. This can be written

$$\text{Generator cost (\$)} = AW \qquad (1)$$

where A is a constant and W is power output in kilowatts.

Where we have a circuit, and a load, connected as in the diagram of Figure 1, it is obvious that the capacitor rating in kilovolt-ampere needed to raise the load power factor to 100 per cent is given by

$$\text{Kva (capacitor)} = \frac{W\sqrt{1-PF^2}}{PF} \qquad (2)$$

This is derived from $\tan\theta = \sin\theta/\cos\theta$, $\sin = \sqrt{1-\cos^2\theta}$, and therefore

$$\tan\theta = \frac{\sqrt{1-\cos^2\theta}}{\cos\theta}$$

where W in equation 2 is power in kilowatts delivered to the load, and PF the load power factor.

Since the cost of capacitors is in general proportional to their kilovolt-ampere ratings for given voltages, we therefore may write the following expression for capacitor cost:

$$\text{Capacitor cost (\$)} = \frac{WC\sqrt{1-(PF)^2}}{PF} \qquad (3)$$

where C is capacitor cost, dollars per kilovolt-ampere.

The total cost of equipment for supplying power W to the given load is the sum of both the generator (or oscillator) cost and the capacitor cost. This total cost can be expressed as follows:

$$\text{Total cost (\$)} = \frac{W(A + C\sqrt{1-(PF)^2}}{PF} \qquad (4)$$

where C is again capacitor cost, dollars

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LAWRENCE M. DURYEE is industrial power engineer with the Connecticut Light and Power Company, Waterbury, Conn.

Acknowledgment is given to E. C. Northrup of Trenton, N. J., for his report on fundamentals.

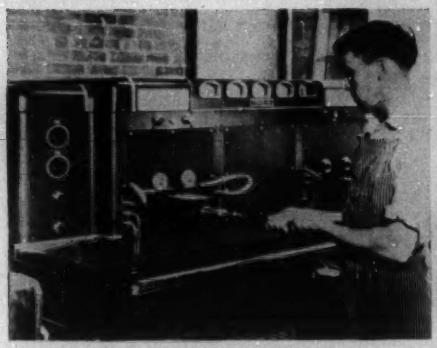

Figure 2. A 9,600-cycle induction heating installation for preheating bolts for hot heading

Figure 3. A 450-kc oscillator and work coil used in instrument manufacture

per kilovolt-ampere, and A is generator cost, dollars per kilovolt-ampere.

As an example, on a 360-cycle frequency changer rated at 250 kilovolt-ampere the total cost of the generator plus a 500-kilovolt-ampere synchronous motor (oversize for plant power factor correction) is about \$7,000 or approximately \$28.00 per kilovolt-ampere of output. This, at 100 per cent power factor load, is \$28.00 per kilowatt. One may take the cost of capacitors at 850 volts and 360 cycles as \$7.00 per kilovolt-ampere, for example. In this particular instance the uncorrected power factor of the load was measured and found to be 0.4. Then, using equation 4,

$A = \$28.00 \qquad C = \$7.00$

$PF = 0.4 \qquad (PF)^2 = 0.16$

Then

$$\text{Total cost} = \frac{W(28.00 + 7.00\sqrt{1-0.16})}{0.4} = \$42.30W$$

If this machine is operated at full load of 250 kilowatt, the total cost is $\$42.30 \times 250 = \$10{,}500$.

If the capacitor constant C is less than the generator constant A it will be cheaper to supply only the power component of the load with a generator of that size, and purchase capacitors for the entire reactive component. If the contrary is true, then the generator (or oscillator) should be of a size to supply the total power and reactive components, leaving the load circuit power factor uncorrected. The foregoing applies to the load circuit in its entirety.

Table I. Band Numbers

Band Number	Frequency	
0	0–10	Cycles per second
1	10–100	
2	100–1,000	
3	1,000–10,000	
4	10,000–100,000	
5	0.1–1	Megacycles per second
6	1–10	
7	10–100	
8	100–1,000	
9	1,000–10,000	
10	10,000–100,000	

Economics of Frequency Selection and Coupling Methods

Theory shows that when the ohmic resistance R measured circumferentially around a pipe, or thin-wall cylinder, which is to be heated above the Curie point is numerically equal to the inductive reactance of the same path then the electrical efficiency is maximum, assuming the resistance of the water-cooled inductor to be zero. This is proved by the following.

Maximum power delivered to the work would result if no reactance were present. This, of course, is not possible, in induction heating. All power is expressed by

$$P = I^2R \tag{5}$$

where I is current and R is resistance. But

$$I = E/Z \tag{6}$$

where E is applied voltage and Z is impedance and

$$P = \frac{E^2R}{Z^2} \tag{7}$$

and

$$Z^2 = R^2 + X_L^2 \tag{8}$$

Therefore

$$P = \frac{E^2R}{R^2 + X_L^2} \tag{9}$$

For any given frequency, maximum power is delivered when $R = X_L$. Hence a frequency is chosen such that

$$R = 2\pi fL \tag{10}$$

where L is self-inductance of the load cross section of the work being heated. This means that the optimum load power factor is 70 per cent.

The formula for load-circuit power factor for a thin-wall cylinder is, by experience and empirical test

$$PF = \cos\theta = \frac{\phi^2 R X_L}{\sqrt{(R^2 + X_L^2)[X_L^2(\phi^4 - 2\phi^2) + R^2 + X_K^2]}} \tag{11}$$

where

ϕ = coupling factor $= d^2/D^2$

where d is mean diameter of cylinder and D is inside diameter of the inductor coil.

Selecting a frequency such that $R = X_L$ then equation 11 becomes

$$PF = \cos\theta = \frac{0.707\phi^2}{\sqrt{\phi^4 - 2\phi^2 + 2}} \tag{12}$$

Varying the coupling ϕ by steps from 1 to 0.5 and substituting these ratios of diameters in equation 12 we get Table II.

These data and table show the rapidity of decrease in the load-circuit power factor as the coupling factor $\phi = d^2/D^2$ be-

Table II

ϕ	ϕ^2	ϕ^4	$\cos\theta$
1.0	1.0	1.0	0.707
0.9	0.81	0.66	0.561
0.8	0.64	0.41	0.425
0.7	0.49	0.24	0.309
0.6	0.36	0.13	0.214
0.5	0.25	0.063	0.141

comes less. The need for close fitting of the inductor coil to the load thus is forcefully made apparent, when one is interested primarily in efficiency.

Based upon the fundamental equation 9 a convenient expression has been developed which recognizes that the *internal* power factor of the work to be heated must be 70 per cent (that is, the resistance of the load itself must equal its inductive reactance)

$$f = \frac{P \times 10^9}{\mu r^2} \qquad (13)$$

where

f = frequency, cycles per second
r = radius of the work in inches
P = resistivity in ohms per centimeter cube
μ = permeability

As an example of this fact, a bolt manufacturer who previously headed bolts by the cold-heading process found, from a study of his operations, that substantial labor-saving money and longer die life would result if he hot-headed bolts before forging, by induction heating. Expensive stress-relieving operations also would be eliminated. He selected, therefore, a motor generator type of equipment producing 9,600 cycles per second, based upon the following:

Diameter of bolts = 1 inch
Radius of bolts = 0.5 inch
$\mu = 1$ (since the forging temperature of 1,650 degrees Fahrenheit is well above the Curie point at which steel loses magnetic properties)

The economic frequency for this application then became, from equation 13

$$f = \frac{140 \times 10^{-6} \times 10^9}{0.5^2} = 5{,}600 \text{ cycles per second}$$

The motor-generator set of 9,600 cycles was the nearest commercial unit available, and after installation, it worked out most satisfactorily. The operational data is tabulated in relation to bolts of 0.875 inch diameter (Table III). It is to be noted that a shunt capacitor of 34.25 microfarads was used at inductor coil terminals, to reduce the kilovars to zero. Figure 2 shows this installation.

Table III. Typical Test Data (9,600-Cycle Motor Generator Set)

Band 3

Diameter of work, in.	0.875 medium carbon
Length to be heated, in.	$1^1/_{16}$
Bore of work coil (inductor block), in.	15/16
Length of work coil, in.	$1^1/_4$
Recess for insertion of rod to be heated, in.	0.03
Power input of 9,600-cycle energy to coil, kw	50
Seconds to heat	1.4
Temperature of work, F	1,650
Field current, amp.	3.6
Size of capacitor at terminals, μf	34.24
Kvar of load circuit, which includes capacitor	0
Line volts at 60 cycles	220
Line current at 60 cycle, amp.	227
Input to motor, kw (220×227×0.80 power factor)	69
Temperature of cooling water, F	300
Rating of 220-volt motor, hp	110
Rating of 9,600-cycle generator, kw	100
Efficiency, per cent—motor to heater coil $\frac{50}{69} \times 100$	72.5

Figure 4. Coils used in instrument manufacture

Figure 5. Sample of damping magnet showing Alnico brazed to steel

Specific Case Histories Including Economic Data Induction

Induction heating is defined tentatively by the AIEE subcommittee on electronic heating as "the heating of a nominally conducting material due to its own internal losses when the material is placed in an alternating electromagnetic field."

The following group of case histories of induction heating equipments utilizing vacuum tube oscillators serves to illustrate the economic advantages—or disadvantages—in specific industries. The outstanding development which this survey revealed, and one which is of special interest, lies in the way in which each particular heat-treating engineer in the industry studied, solved his own work-handling and fixture problems. Moreover, the ingenuity of coil design was, in many cases, unusual, and the method by which these coil designs were developed so successfully was, in almost every case, the process of trial and error. General fundamentals were adhered to, of course, such as thin-wall coils for close coupling; multiple-turn coils in place of one turn, where possible, for the nonmagnetic products, to increase I^2R heat absorption; and adequate turn-to-turn insulation where voltages demanded. In general, however, emphasis was placed upon ease of operation by the unskilled worker who performed the daily work, rather than on the objective of realizing the highest coil efficiency. The results, in terms of final product cost, when heated by induction, prove the practicality of this approach.

Table IV is a recapitulation of results obtained on a variety of products at the Bristol Instrument Company, Waterbury, Conn. It is to be noted that total annual savings for brazing and spot annealing the many instrument parts in this plant amounted to approximately $10,000 per year, which retired the investment in the high frequency oscillator in one year. A second induction heating oscillator has been ordered of exactly the same specifications as follows:

Output—15 kw at 450 kc (band 5)

Input—25 kva, 3 phase, 60 cycle. Load power factor of oscillator is 95 per cent at the 60-cycle input side

Remarks. In the case of the damping magnet (item 7 in the table) a high-cost steel alloy formerly was used when brazing was performed by hand. The use of the localized and extremely rapid heat furnished by the induction process enabled the user to substitute a cheaper steel. Further, this steel part is brazed inductively to the Alnico, which possesses a Curie point of ±900 degrees Fahrenheit without damage to the magnetic property of the Alnico. Figure 3 shows the oscillator and work-coil; Figure 4 the various coils used in this plant; and Figure 5 the damping magnet with Alnico brazed to steel.

Table V shows the results at the Cleerman Machine Tool Company, Green Bay, Wis. The oscillator hardens SAE 4150 steel gears, using 15 kilowatts of out-

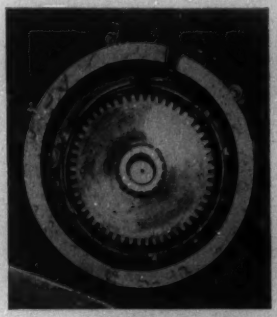

Figure 6. Gear hardening machine uses 525-kc radio-frequency energy

Table IV

Product	Use Made of Product	Operation Performed	Previous Time, Hours Per 100 Pieces	Induction Time, Hours Per 100 Pieces	Type of Coil	Previous Unit Cost, $ (Factory)	Induction Unit Cost, $ (Factory)	Annual Savings, $ (Factory)
Protection wells	Thermocouple protection	Braze cap to tube	2.0	1.1	Single turn round coil	0.068	0.037	62.00
Spring 44259	Roll temp thermocouple part	Spot anneal 4 points	0.7	0.2	Two single turn coils	0.024	0.007	0.43
Well for low range differential recorder 55299 53831 53830	Part of low range differential recorder	Soft solder 2 lead pipes to well and 2 fittings	50	17.4	Single turn round coil	1.70	0.50	166.50
Lever 54323	Valve positioner lever	Braze shaft to lever	0.72	0.4	12-turn coil—12 pieces done at once	0.025	0.014	5.50
Brass blank 73931	Bent from bracket	Spot anneal for bend—2 spots	Impossible without induction	0.3	Two single-turn coils		0.010	
Vat fitting 81018 82059	Fire end part	Braze brass flange to copper tube	201	6	One 8-turn coil	0.68	0.20	44.64
Damping magnet 74368	Damping device Alnico to steel	Silver braze 2 Alnico magnets to cathode ray steel yoke	34.8	5.0	Two pancake coils	1.18	0.17	1010.00
Pressure connection 40546	Connection for cathode ray gauge	Soft solder tube to fitting	1.8	1.0	Single turn coil	0.061	0.034	13.50
Sep. socket 9157	Socket for sensitive bulb	Braze bronze head and cap to tube	20	2.2	Multiple coil	0.68	0.075	55.35
Umbrella flange 83063	Dairy bulb use	Silver solder flange to tube	40	1.5	Metal coil	1.36	0.051	261.80
Pilot valve mounting 52052	Dairy bulb use	Braze fitting to brass forging	2.0	1.1	2-turn coil	0.068	0.037	124.00
Stud 84645	Cycle contr. part	Spot anneal for bend	Impossible without induction	0.25	1-turn coil		0.009	

Total annual savings including parts not mentioned—approximately $10,000.00

Figure 7. Hatchet heads tempered with induction heating doubled production

Figure 8. Small parts are silver soldered with 20-kw 450-kc oscillator

put energy at 525 kc (band 5). Figure 6 is a view of this installation. The plant saves a total of $1.70 on each gear hardened, and produces a far more accurate gear by reason of reducing distortion to a minimum.

Table VI applies to the tempering of hatchet heads in the plant of the Mitchell and Unsinn Company, Washington, D. C. This plant formerly flame-tempered the heads. With this flame process, one man placed the untempered hatchets on an endless chain. The hatchets then were conveyed down a chimney-like structure at the rate of 8 feet per minute and were thus preheated before they entered the flame. After passing through the flame, the products fell on a series of baffles inside the chimney and from there, they slid down into an oil bath. This produced 650 hatchets per day. Figure 7 illustrates the installation.

When flame-tempering was replaced by induction heating using a 20-kw-output oscillator, only one operator was needed in place of two formerly and the hatchets came out clean, uniformly tempered, and at twice the production rate per day. The induction coil produces 1,300 hatchet heads daily.

Table VII emphasizes the principle demonstrated in Table IV, that induction heating is justified economically not only for large production runs, but also for a wide variety of small-lot requirements. Products in this column are brass parts. Note the simple setups used since expensive fixtures were not justified by the small production runs. Here a nipple on the end of a cable guide is silver soldered. Multiple-turn coils of conventional design enable these and other applications to be done in a fraction of the time formerly required. For example, by using a standard 20-kw 450-kc oscillator, a leading manufacturer of electric equipment has the time study figures to report as typical. Time values shown are from detailed time studies, and they include the complete production cycle of handling, heating, and removal. Figure 8 is a view of one of the operations.

Table VIII illustrates the use of a radio-frequency oscillator applied to continuous production with the use of an indexing turntable to hold the product and to move it automatically into the work coil. The Benrus Watch Company, Waterbury, Conn., formerly brazed nickel-silver watch cases to stainless steel backs by hand, using irons. After several months of experimental work and the ingenious application of induction brazing to the product by their development engineer, a 375-kc oscillator was set up and obstacles successfully overcome. In addition to the annual saving of $7,800 per year, which retires the investment in the first 12 months of use, fewer rejects are experienced with induction brazing. Further, distortion is completely eliminated with the clever fixture designed for this job. In addition, the user is no longer dependent upon skilled labor since unskilled girls now are employed who merely preset the brazing wire and load the machine. Four cases are brazed in each 15-second heating cycle. A gas

Table V

Cleerman Machine Tool Company, Green Bay, Wis.—Band 5

Gear data

Number 20-19M. Idler gear assembled with number 20-19K clutch gear
6 1/2-in. diameter by 3/4-in. face
Hardness required—65/70 scleroscope
Material—SAE 4150 steel treated 230/250 Brinell hardness in the bar.

Coil data

3/16-in. copper tube flattened to 1/8 in. and wound edgewise to 7 1/2-in. inside diameter by 1-in.-long, 3-turn coil.

Operation

Total heating cycle—67 sec
Quench—55 sec
Work revolves in inductor coil and is water-quenched.

Comparative cost data; finish gear 19M

Old method (furnace heat):	
Heat treat	$0.53
Internal grind	0.64
Finish clutch 19K	0.11
Heat treat	
Press together and internal grind	0.77
Total	$2.05
Induction heat:	
Broach clutch 19K	$0.06
Induction harden	0.03
Broach gear 19M	0.13
Induction harden	0.13
Total	$0.35

Saving—$2.05–$0.35 = $1.70 per unit.
In addition to this saving of $1.70 on each gear unit a more accurate gear is produced.

Table VI

Mitchell and Unsinn Company, Washington, D. C.—Band 5

Former Method	
Fuel	$0.90
Labor of 2 men to operate machine	1.90
Labor of 1 man to clean off oil	0.90
Electric power for conveyor	0.04
Naphtha for cleaning off the oil	0.05
Total	$3.79 per hr
Induction Heat	
Control circuit electricity	$0.04
Oscillator power	0.135
Labor, one man	1.00
Water for cooling circuit	0.108
Tube replacements	0.10
Total	$1.38 per hr

Table VII. Cost Data Per Hour
Band 5

	Torch Braze	Induction Braze	Per Cent Increased Production
Cylinder assembly	$0.567	$0.095	605
Pivot elbow	0.190	0.119	160
Tube head	8.49	1.78	475
Hose assembly	0.150	0.086	174
Conductor and plug assembly	0.094	0.054	174
Push rod assembly	0.035	0.021	166
Echo box cap boring	0.100	0.074	135

atmosphere is bled into the hood which surrounds the work coil of 85 per cent nitrogen and 15 per cent hydrogen to produce a clean product. The machine is rated at 20 kilowatt output and uses a coupling transformer of a turn ratio of 22 to 1 at the work coil. Figure 9 is an illustration of the setup.

Dielectric Heating

Dielectric heating is defined, tentatively, by the working group of the AIEE subcommittee on electronic heating as "the heating of a nominally insulating material due to its own internal losses when the material is placed in a varying electric field."

While frequency selection is not as simple as in the general field of induction heating, there is nevertheless a means for a definite conclusion to be reached based upon the fundamental concept of dielectric heating.

Maximum heating speed is related to frequence as follows:

$$\text{Time } T = \frac{0.43K \times \text{temperature rise} \times S \times D}{f \times C \times PF} \tag{14}$$

where

f = frequency, cycles per second
T = time in seconds
S = specific heat
D = density in pounds per cubic foot
C = dielectric constant
PF = power factor of material between the electrodes (the work to be heated)
K = a constant related to the shape and thickness of the work

and temperature rise is in degrees Fahrenheit.

Thus, the higher the frequency in general, the shorter is the heating time. However, since the limit of frequency is the available and efficient power output from commercially obtainable oscillators, a frequency of approximately 30 megacycles is the approximate top limit for industrial purposes. This frequency recently has been fixed at 27.32 megacycles by the Federal Communications Commission. A still more simplified form of equation 14 is submitted which relates heat absorption to frequency and voltage as follows:

$$H = C \times LF \times F \times V^2 \tag{15}$$

where

H = Btu per second per pound of material being heated
C = constant varying with density and specific heat of the product
LF = loss factor expressed as a decimal (a product of dissipation factor and dielectric constant)
F = frequency in megacycles
V = volts per inch across the piece being heated

From experimental work and practical application, predominantly in the field of preheating plastic preforms in the compression molding industry, we can apply known values in equation 15 as follows, for a typical compound of cellulose-filled vinyl acetate heated at an applied power rate of 1.5 kw per pound, at a voltage gradient of 2,400 volts per inch giving 1,015 Btu per second. Inserting these data and solving for f with varying values of V, the data in Table IX have been prepared. Since 3 kv per inch is the maximum voltage stress possible without arcover, it appears that from 14 to 28 megacycles is the approximate practical frequency range when conventional electrodes are used. In band 9, which to date has had its application in the field of high speed commercial cooking, wave guides are employed and energy is absorbed by the food in a combination of both induction and dielectric heating.

The application of dielectric heating processes to industry, while newer than induction due to the more recent development of power tubes for oscillator circuits in the megacycle band, nevertheless has expanded rapidly. The heating of nonconducting materials by conventional means always has presented a problem due to the critical nature of the surfaces of these products which entailed slow heating by convection, radiation, or conduction. This is due, of course, to limitations of surface temperatures and required long soaking periods for the product to be brought to temperature. This time cycle in the case of nonconducting ma-

Table VIII. Cost—Cents Per Piece
Benrus Watch Company—Band 5

	Former Hand Braze Method	Automatic Induction Braze
Direct labor cost	1.00*	0.2†
Investment cost at 25 per cent fixed costs		0.125
Energy cost	0.04	0.04
Tube replacement		0.025
Total	1.04	0.39

Saving—1.04 cents—0.39 cent = 0.65 cent per piece for 1,200,000 pieces per year, or $7,800.

* Five men per thousand pieces.
† Two girls per thousand pieces.

Figure 9 (left). Stainless-steel watch cases brazed to nickel silver "boxes"

Figure 10. Foundry core baking improves castings at reduced cost

Figure 11. Plastic "pills" are preheated by dielectric heating

Figure 12. Radaranges at Hotel Statler cook complete meal in 60 seconds

terials was lengthened further with conventional heating due to thermal insulating properties of these materials. Therefore, with the advent of megacycle heating from electronic oscillators, industries immediately seized upon the opportunities offered by this new process of internal heating and thereby expanded their production. The case histories presented in the following are typical of production increases made possible by dielectric heating. There are, of course, many materials which possess unsatisfactory heat absorption factors which cannot at this writing be heated economically in radio-frequency fields.

Foundry Application—Dielectric Core Baking; Band 7

In the field of core baking in foundries, the new development of dielectric baking is capturing the interest of foundrymen. At the Lehigh Foundry, Lehigh, Pa., continuous dielectric baking of cores has been in practical operation for several months replacing conventional oven baking. It should be borne in mind that any new process necessitates extensive research which in this case resulted in some substantial changes in foundry practice. Notable among these changes were the substitution of materials (wood) for core supports, improved synthetic resin binders instead of oils, and plastic core driers instead of metal. The following resultant advantages have been reported though it is too early for specific dollar evaluations to be put on these items.

1. Direct labor saving.

(a). Green sand handling reduced.

(b). Mulling of mixtures reduced from 12 minutes to 5 minutes per mix.

(c). Loading of a conveyor from the core benches reduces handling of cores.

(d). Baked core handling reduced due to finished cores being conveyed directly to inspection tables.

(e). Loading and unloading of racks is eliminated.

(f). Finer finish on core surfaces reduces labor of removing metal fins on the castings.

(g). Shake-out labor is reduced due to the core residue becoming merely a fine powder easily removed from the metal cavities.

2. Improvement in collapsibility due to distillation of cores.

3. Increase in yield due to reduction in rejects resulting in decrease of rejects from a former 30 per cent figure to 10 per cent with dielectric baking of cores. Uniform baking accomplished by the dielectric method accounts for this.

4. Negligible "gassing" results from a minimum of volatile content in the resin binder. This reduces turbulence when metal is poured and improves the metal surfaces.

5. Reduction in number of core mixtures due to standardization with dielectric core baking regardless of the shape of the core.

6. Reduction in driers in stock because cores are baked in two minutes instead of two hours.

7. Overbaking eliminated. No matter how long the cores remain in the high frequency field, heat absorption of the cores stops before dangerously high temperatures are reached. This is due to the drop in power factor of the synthetic resin as temperature increases.

8. Fuel consumption is relatively low.

Table IX. Relation Between Kilovolts and Megacycles

Kv Per Inch	Frequency, Megacycles
23.2	0.5
11.6	1.0
6.2	3.5
4.4	7.0
3.1	14.0
2.2	28.0

Heat supplied is proportional to the actual load. Electrodes remain at room temperature and no external heat is lost save in the radio-frequency circuit and radiation of product.

The capacity of this production unit is 500 pounds of cores per hour. The investment is $11,000. The full-load power input is 25 kw. The output is at 15 megacycles. Figure 10 shows the core-baking machine with the operator loading the product onto the moving conveyor. Cores are about to enter the shielded electrode chamber.

Plastic Molding; Band 7

Preheating of plastic preforms at the Bryant Electric Company, Bridgeport, Conn., is illustrated in Figure 11. Preformed "pills" are heated uniformly to a plastic state before being transferred to the compression molding machine. Substantial reductions in total molding time result. In addition, slender pins and inserts can be used in the molds without fear of shearing. Table X lists the results in percentages. A standard oscillator of 5-kw output is used here. On special items such as the heavy-section motor housings for vacuum cleaners, preheating with megacycle energy increased the production 169 per cent per man-hour.

Commercial Cooking; Band 9

Radio-frequency heating has entered the kitchen of hotels and restaurants. The Radarange which has been used successfully by a well-known hotel chain for rapid cooking of uniformly mixed foods now is finding its way into smaller restaurants as well. The range uses the

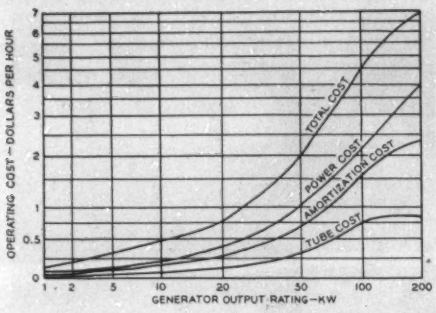

Figure 13. Radio-frequency-generator operating cost curve

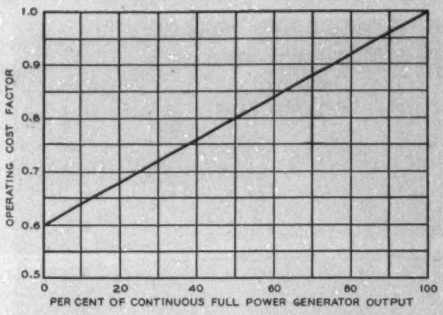

Figure 14. Operating cost factor for fractional generator output

type *QK217* magnetron. It has a maximum power input of 7.6 kva at 70 per cent power factor, 230 volts (to the range) single phase, 60 cycle. The output is 1.7 kw of energy at 3,000 megacycles. A complete meal consisting of breast of chicken or Virginia ham with corn and tomatoes is cooked in 60 seconds. Several additional installations are being made in the hotels of the chain which pioneered in this form of cooking, which indicates the degree of acceptance of this new use of radar frequencies. Proper wave guides direct this energy to the food which is placed in the oven compartment. Figure 12 is a view.

While no direct cost comparisons have been made as yet, a typical small restaurant which serves between 300 and 400 patrons per day utilizes this range for all the cooking and supplements the Radarange by a conventional electric broiler in order to put the familiar broiled surfaces on the food prepared in the radio-frequency range. A combination of both dielectric and induction heating accomplishes the high speed cooking from the extremely high frequencies of band 9.

Radio-Frequency Cost Data—Vacuum Tube Oscillators

In general, the operating cost in dollars per hour for the standard commercially available oscillators may be determined from graphs, Figure 13. Tube cost, amortization cost, and power cost are plotted for the various size oscillators. Total cost for a given application must include, of course, labor and work handling cost as well as the increased fixed cost on work handling investment. The curve of Figure 13 is based upon

1. Current prices for radio-frequency generators (minus accessories).
2. Amortization based upon 30,000 hours of operation.
3. Input power cost at one cent per kilowatt-hour.
4. Oscillator tube life of 5,000 hours.
5. Rectifier tube life of 3,500 hours.

Furthermore, since the curve, Figure 13, is based upon 100 per cent duty factor, the operating cost for other than 100 per cent duty factor can be found from curve, Figure 14. Values found from Figure 13 should be multiplied by the "operating cost factor" from Figure 14 which is an average of the cost factor of standard sizes of oscillators now available. Over-all operating efficiency, including stand-by power, is accounted for in this curve. It is accurate to within 10–15 per cent.

Table X

Material saved	12 per cent
Production increased	25 per cent
Mold life increased	30 per cent
Mold cost decreased	67 per cent
Cure time decreased	91 per cent

Conclusion

High frequency heating is not a cure-all for every industrial or commercial heating problem. Moreover, because of the high investment represented in this type of equipment, the financial hazards in investing in it are obviously greater. If a purchaser of a high-frequency heating tool unexpectedly loses an important contract, for a long production run, of products readily adapted to the machine, the financial burden may be great and the decision to purchase may prove to be unwise. This is due to the fact that the capacity cost for equipment per Btu per unit of time, which these machines represent, is obviously greater than with other comparable heating tools. This entails higher annual fixed costs on the equipment investment. This is especially true of the obsolescence factor (amortization) which in high-frequency heating is obviously greater due to the rapid developments and changes compared to the more conventional heating tools.

Despite these inherent hazards, the use of induction and dielectric heating is expanding rapidly due to advantages inherent in the processes. In induction, the major factor is the localized heating obtainable with extreme speed and control of the depth of heat penetration. In dielectric, the uniform, rapid heating accomplished throughout the mass by "molecular friction" when the product is of uniform loss factor represents a phenomenon not associated with other forms of heating. The case histories herein presented are typical of the operations in this combined field of radio frequency heating and were selected at random from the thousands of installations throughout the country. In specific cases, equipment investments have been retired in as short a time as a few months. From the data presented, however, it is seen that a one or two year retirement is the general trend when high-frequency heating is applied with sound engineering to industrial heating applications.

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No Discussion

Electrical Insulation Deterioration Treated as a Chemical Rate Phenomenon

THOMAS W. DAKIN
NONMEMBER AIEE

Synopsis: The basis is presented for a more accurate interpretation of the results of physical type tests to measure the thermal aging of insulation together with a more accurate method of applying the results of such tests to predicting insulation deterioration in practice. Since the observed physical changes during thermal aging are the result of internal chemical changes in organic material, it is shown that the theory of chemical reaction rates can be applied to analyze experimental data on aging. The approximate 7 to 10 rule for the temperature coefficient of deterioration rate is replaced by a more accurate theoretical expression. Various examples of insulation life tests are analyzed using the graphical methods outlined in the paper. The chemical rate theory interpretation of thermal aging offers a more satisfactory method for extrapolating the results of limited aging tests of insulating materials so they can be applied to predicting amounts of thermal aging in high temperature cycles.

THE PURPOSE of this paper is to present a logical method of interpreting the measured changes in physical or electrical properties of insulating materials during thermal aging. This method is based on the assumption that the observed physical changes are the result of internal chemical reactions which obey well established theoretical laws. In place of the commonly used rule referred to in the AIEE Standards number 1 which predicts a decrease in insulation life by one half for each 7 or 10 rise in temperature, this analysis offers a more accurate equation for the temperature coefficient of insulation life or conversely of the rate of deterioration.

The method of analysis to be presented in this paper is intended to give a more accurate means of extrapolating the results of controlled temperature accelerated aging tests, and of applying the results of these tests to predicting the life of insulation in electrical apparatus under conditions the same as in those in the test, but with a varying or cycled temperature.

In illustrating the application of this method of analysis of deterioration rate data, examples of a variety of insulation deterioration tests will be discussed. It is assumed that it is possible to know the specific reason for electrical failure in the form of a short circuit. Often this reason is a mechanical failure in the insulation as a result of the decrease in the tensile strength or flexibility. In other cases the failure is electrical in nature as a result of the decrease in electrical resistance to too low a value or similar effect. This paper will not discuss the problem of selecting the test or condition of the physical property of the insulation which best corresponds to the failure point or imminent failure point in the apparatus. But this paper starts out from the point where such a test has been selected and discusses the use of data from controlled test experiments.

Chemical Processes in Insulation Deterioration

The chemical changes are many fold which may occur in organic insulating materials with time under conditions of elevated temperatures and applied voltage, with or without the presence of an oxidizing atmosphere and numerous catalytic substances including the copper and iron of the apparatus, itself. The most common form of deterioration is slow oxidation which introduces acid groups into the polymeric or liquid insulation, which acid groups increase the conductivity and power factor of the insulation. The oxidation also splits up the polymer chains in resin insulation and decreases the tensile strength.

Another important type of deterioration is the brittle hardening of insulation as the result of loss of plasticizer and excessive oxygen cross-linking of polymer chains in plastic insulation. In the first place, it is merely a slow evaporation of the lower molecular weight substances in the plastic which, when present, renders it flexible. In the latter case, the introduction of oxygen increases the cohesion between some neighboring chains while, at the same time, breaking the chains to make them shorter. This has the net result of increasing the hardness, but decreasing the strength to bending or stretching.

A third type of deterioration is the purely thermal or internally catalyzed depolymerization of plastic insulation. This phenomenon where polymer chains break into shorter units occurs in all polymers including cellulose at elevated temperatures even in the absence of oxygen. This reaction is, in general, a much slower one than oxidation, but at high temperatures it becomes an important factor.

It is not the purpose of this paper to discuss in detail specific chemical reactions which deteriorate insulation. A few types have been mentioned. There are others including electrolytic reactions and spark or corona induced reactions. It is rather the purpose of this paper to discuss subjectively chemical reaction rates as they affect the measurable physical properties of insulation.

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THOMAS W. DAKIN is with the physical section, insulation department, research laboratories, Westinghouse Electric Corporation, East Pittsburgh, Pa.

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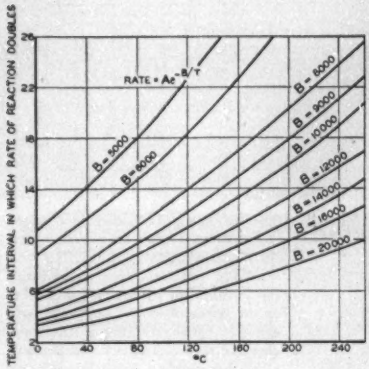


Figure 1 (left). Relation between the temperature coefficient of a chemical reaction and rules of thumb for rate doubling

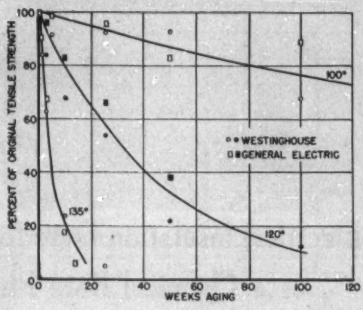


Figure 2. Decrease in tensile strength of cellulose paper in oil under nitrogen with time of aging at elevated temperatures both plotted for linear scales

Chemical Rates in Insulation Deterioration

Physical chemists have for many years been investigating the rates of many different types of reactions and their conclusions can be applied in a rather simple and general manner to the special problem of electrical insulation deterioration. Their investigations have been concerned with the rate of change of concentration or amount of certain constituents of chemical mixtures or solutions as a function of time and, also, with the variation of this rate of change with temperature. Their method of approach can be applied to the problem of insulation deterioration if we know or can assume that the physical property in which we are interested is proportional to the concentration of an important constituent or is a simple function of the concentration. Expressing this mathematically, we assume that:

$$P \sim C \text{ or } P = f(C) \tag{1}$$

where P is the magnitude of the physical property in which we are interested and C is the concentration of an important chemical constituent of the insulation which is being changed by the thermal aging.

For example, the dependence of tensile strength of paper is probably somewhat more complicated,[1,2] but it has been found to be fairly correct to assume that the tensile strength of paper decreases in proportion to the average number of links in the cellulose, molecule chains. In the process of deterioration, this chain length decreases progressively, the links or interatomic bonds being broken at random places along the chain. The number of links in the cellulose chain are analogous to the concentration of a chemical species, which is being transformed by the reaction.

In general, the instantaneous rate of change of the number of molecules undergoing a transformation with time is proportional to the number present raised by some exponent or, stating this mathematically,

$$\frac{dC}{dt} = -KC^n \tag{2}$$

where C is the variable number per unit volume or concentration, K is a rate constant and n is an exponent which determines the order of the reaction. Usually, but not always, n the number of the same kind of molecules reacting with each other and this is usually 1 and hardly ever more than 3. The rate constant K is dependent on the temperature, the concentration of catalysts and other chemicals entering into the reaction, which in the equation 2 are assumed to be a constant throughout the reaction. A more elaborate treatment would consider the time variation of these other components of the reaction.

Integration of equation 2 in the case of a first order reaction ($n=1$) gives

$$\log_e C = -Kt + \log_e c_0 \tag{3}$$

$$C = c_0 e^{-Kt} \tag{3a}$$

It can be seen from equation 3a that the concentration of a chemical being transformed in a first order reaction decreases exponentially with the time. Unfortunately many processes of deterioration in insulation do not follow a simple first order reaction mechanism, or the change in the concentration of other chemical compounds entering or affecting the reaction must be considered. In general, however, a mathematical function of the concentration of the most important chemical constituent can be found which is an integral of equation 2 regardless of what the order of the reaction is or whether the rate constant varies with the time

$$f_0(C) = -K_e t \tag{4}$$

In equation 4, the rate constant K_e is temperature dependent, but not time dependent. The form of function $f_0(C)$ is assumed to be independent of temperature. In the case of the simplest first order reaction, equation 4 would be the same as equation 3 and our function $f_0(C)$ is simply $\log_e (C)$.

Now by introducing the assumed relation between the physical property and the concentration of the most important chemical constituent given in equation 1, a simple relation between the physical property and the time is obtained:

$$f_0'(P) = -K_e t \tag{4a}$$

A graphical plot of $f_0'(P)$ versus time gives a straight line, and a straight line can be readily extrapolated to predict the condition of the physical property at any later time after the slope is established by a short time test. If the mathematical form of the function $f_0'(P)$ is not known, the function must be established on a graphical scale by measuring and plotting the change in the physical property, P, with time at an elevated temperature where the test can be completed in a reasonable time. An example of this is shown in Figures 4 and 5 to be discussed later.

Chemists have shown that the temperature dependence of a reaction rate very closely follows an exponential law:

$$K_e = Ae^{-B/T} = \text{a reaction rate constant} \tag{5}$$

or

$$\log_e K_e = -\frac{B}{T} + \log A$$

In this expression A and B are constants and T is the absolute temperature. It is important to note that the absolute temperature scale is used, consequently the temperature intervals at high temperatures have, percentagewise, less effect on a reaction than the same temperature intervals at low temperatures. This fact is neglected in the commonly used rule of thumb,[3,4] where the temperature interval for which a reaction rate doubles is used. Such rules may be correct for one temperature, but quite incorrect for a higher or lower temperature. The correlation between the exponential law of equation 5 for some commonly encountered values of the constant B and the temperature interval for which a reaction rate doubles is shown in Figure 1. The theoretical exponential law is recommended as being more accurate, remembering that each reaction may show a different temperature coefficient. Gross errors are likely to be encountered in applying the same temperature coefficient under all conditions.

A combination of equations 4a and 5 gives another useful relation.

$$\frac{f_o'(P)}{t} = K_o = A\epsilon^{-B/T} \qquad (6)$$

If one selects a particular value for the property, P, as, for example, 20 per cent of the original tensile strength as a limiting danger point for paper insulation, it is apparent that $f_o'(P_{20\ \text{per cent}})$ may be considered a constant and the time to reach 20 per cent tensile strength (this might be selected as the life of the insulation) considered as a reaction rate constant and treated as a function of the temperature:

$$\frac{\text{constant}}{t_{20\ \text{per cent}}} = \epsilon^{-B/T} \qquad (7)$$

$$\log_e t_{20\ \text{per cent}} = \frac{B}{T} + \log_e \text{constant} \qquad (7a)$$

Therefore, the logarithm of the time to reach a particular value of tensile strength (20 per cent in this case) is proportional to the reciprocal of the absolute temperature, and a linear graph will furnish a more accurate method of extrapolating high temperature measurements to low temperatures. See Figure 8 for an example of this.

The implication of equations 4a and 6 is that each magnitude of the physical property corresponds to a particular concentration of the significant chemical constituents in the insulation, and that this concentration is the same for the same magnitude of the physical property whether attained at high or low temperatures or by a cycled temperature.

If more than one chemical reaction proceed simultaneously, and if these reactions have different temperature coefficients, a plot of the logarithm of the reaction rate constant or the time to reach a certain state of deterioration against the reciprocal of the absolute temperature may not fall on a straight line. It is however, still preferable to plot the data than to rely on an 8 or 10 degree rule.

Examples of Insulation Deterioration

In all tests of insulation deterioration, there is likely to be found a rather wide dispersion of test results, as a consequence of inadequate control in the preparation of the samples or of inadequate control of the aging conditions such as temperature or atmosphere to which conditions the life or rate of deterioration are very sensitive. Another source of dispersion of test results is in the method of test of the physical property or end point which may throw too much weight on the operator's technique or judgment. For these reasons, it should be emphasized that test results should be interpreted in a statistical manner with sufficient duplicate tests run to obtain a good average, and recognition taken of the statistical probability of deviations from this average in individual cases.

As one example of the method of treatment of deterioration rate data outlined here, some recent experiments on the aging of cellulose insulation will be discussed. The result of these experiments illustrate the type of data which can be obtained, where a physical property is measured as it changes progressively during the period of deterioration.

The AIEE transformer subcommittee group on co-ordination of aging of transformer insulation reported[5] a series of

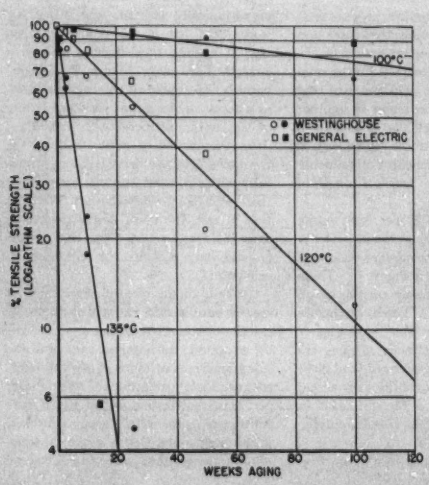


Figure 3. Decrease in tensile strength of manila paper in oil under nitrogen plotted on a logarithmic scale with time of aging at elevated temperatures plotted on a linear scale

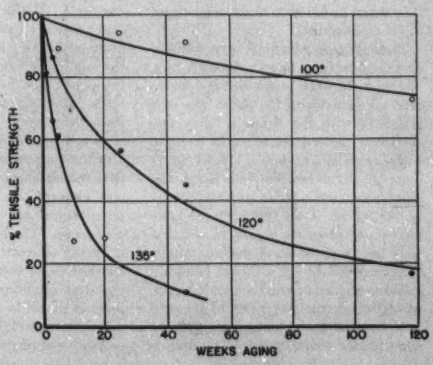


Figure 4. Decrease in tensile strength of manila paper in oil under nitrogen, together with cloth and pressboard, with time of aging at elevated temperatures both plotted on linear scales

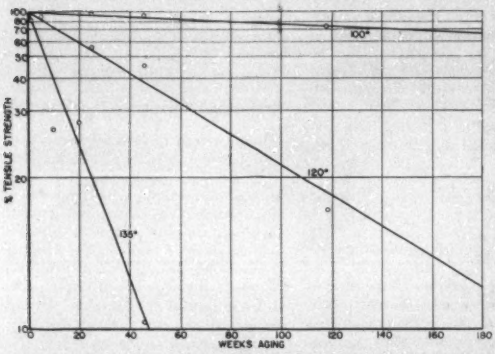


Figure 5. Decrease in tensile strength of manila paper in oil under nitrogen together with cloth and pressboard, plotted on a synthetic functional scale, with time of aging at elevated temperatures plotted on a linear scale

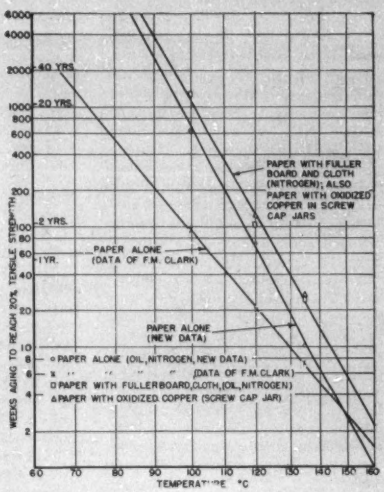


Figure 6 (right). The time in weeks aging at elevated temperatures for manila paper to decrease under various conditions to 20 per cent of original tensile strength, plotted on a logarithmic scale versus the reciprocal absolute temperature

tests showing the decrease in tensile strength of dry paper sealed in oil under nitrogen and aged at elevated temperatures. The data obtained from two of the laboratories which ran the tests are plotted in Figure 2. The slope of the curves of tensile strength versus time indicated a possible first order reaction law dependence as in equation 3 and the plot of the logarithm of the tensile strength versus time in Figure 3 confirmed this by displaying a fair fit to a straight line at each temperature. The spread of the data from the different laboratories indicates the order of reproducibility of this sort of data. Similar data reported[6,7] by F. M. Clark using the same conditions of aging when replotted as in Figure 3 were also found to follow a linear dependence at all temperatures.

The conditions of aging are very important in affecting the deterioration rate. For example, when pressboard and cotton tape are sealed with the paper tape in a test otherwise the same as that just described, the decrease in tensile strength is less rapid and no longer follows a first order chemical reaction rate law. The data from such a series of tests run by L. McCulloch of the Westinghouse laboratory are given in Figure 4. Similar tests had been run for a shorter time by Vogel and Sloat.[8] A scale can be established as implied by equation 4a, however, which transforms the curves of Figure 4 into straight lines, which can then be more easily extrapolated. The tensile strengths of Figure 4 then can be replotted in Figure 5 on this graphical scale. Where no first or second order reaction rate law is obeyed as in this case, it must be surmised that some other chemical whose concentration is changing is affecting the reaction. In other words, the constant K of equations 2 and 3 is no longer a constant or there may be several reactions with different reaction rates proceeding simultaneously to affect the physical property which is being measured.

In this particular case, the water and acidic products of decomposition of the paper tape may be absorbed by the relatively larger amount of pressboard and cloth present. It was found in another test by L. McCulloch that attaching a cold condenser tube to the sealed system had a similar beneficial effect on reducing the rate of decrease of tensile strength of the paper tape.

The time to reach 20 per cent tensile strength of paper under various conditions is plotted as a function of reciprocal absolute temperature in Figure 6. These data show a linear relation on this graph and extrapolation to lower or higher temperatures can be done with greater confidence. The conditions of aging are obviously of great significance, and close comparison of test conditions to actual operating conditions of the insulation in practice should be made, before applying deterioration rate data.

Another example of an insulation deterioration which follows a first order reaction rate law is the arbitrarily designated static life test run by L. E. Frost of the Westinghouse laboratory on insulating varnishes. In this test the varnish coated on a brass rod is aged at elevated temperatures and then periodically immersed in a salt water solution and the resistance between the salt water solution and the rod through the varnish is measured. In such an experiment the resistance of all common organic varnishes decreases logarithmically with the time. In other words, the log of the resistance when plotted against time linearly follows a straight line as in Figure 7. Silicone varnish films tested in this manner show, as a result of the initial period of curing of the resin, an increase in resistance. After this period of curing is over, the resistance here also decreases logarithmically with time, but at a much lower rate than with common varnishes. In this type of test, the point where the resistance has decreased to 1 per cent of the original value is arbitrarily selected as an end point.

Analysis of the rate of weight loss of organic varnish films shows that no simple reaction rate law is followed. This might be expected considering the probable simultaneous occurrence of several different reactions: oxidation and evaporation and depolymerization, all of which may contribute to the weight loss. However, it has been found that a graphical function similar to that of Figure 5 can be

established to describe the weight loss variation with time. If the weight loss data are plotted on this graphical scale against the time, the data at all temperatures fall on straight lines which permit easy extrapolation.

In many tests of insulating materials, only a failure point is measured, and there is no graph of the gradual decrease of a physical property. Nevertheless, according to the interpretation of equation 6, the time to reach this failure point is reciprocally proportional to the rate at which this point is reached. Therefore, the reciprocal of the time to reach the failure point can be considered as a reaction rate constant, and the failure point corresponds to a particular state of chemical composition, this state being the same regardless of the rate at which it was reached. In Figure 8 are plotted on one graph the results of several types of insulation deterioration tests. The common ordinate is the day's time to reach the arbitrary failure point. In the case of the d-c capacitor lives shown by curves *a* and *b*, this failure point is truly the life of the insulation to short circuit. In this case, electrochemical action is probably a contributing factor.

The abscissa scale of Figure 8 is the reciprocal absolute temperature, which is the proper scale to use, but the corresponding centigrade temperatures are given for convenience.

If the particular state of deterioration which is measured always corresponds to a particular extent of chemical reaction in the insulation, it is not essential to know just what the chemical reaction was which resulted in the deterioration to apply this treatment of the data. A curved line on such a plot as Figure 8 indicates more than one reaction is taking place, the respective reactions having different temperature coefficients.

Integration of Deterioration over a Variable Temperature Load Cycle

If the electrical apparatus is subject to intermittent loading with temperatures of the insulation during the overload cycle exceeding those normally encountered, the data given in this paper or data similar to it may be readily used to compute the deterioration during the excessive temperature period. Let us assume that the temperature of the insulation during the overload cycle has been determined, and that the temperature variation with time is that given in Figure 9a with the normal, continuous operating temperature at 90 degrees centigrade at the hottest spot. At this temperature the expected life of the paper insulation to reach 20 per cent tensile strength would be about 76 years, under the conditions of the test when fullerboard and cloth are included with the paper in a nitrogen filled transformer. Excessive temperatures exceeding 90 degrees centigrade for tempo-

Figure 8. Days life (logarithmic scale) of various forms of insulation versus reciprocal absolute temperature

(a) D-c capacitor life at 1,000 volts per mil no inhibitor. (Data from L. J. Berberich and R. Friedman[13])

(b) D-c capacitor life at 1,000 volts per mil stabilizer added. (Data from L. J. Berberich and R. Friedman[13])

(c) American Society for Testing Materials flexible life of organic varnishes (Westinghouse #7559-1 and #7826-3). (Data from P. Demmler)

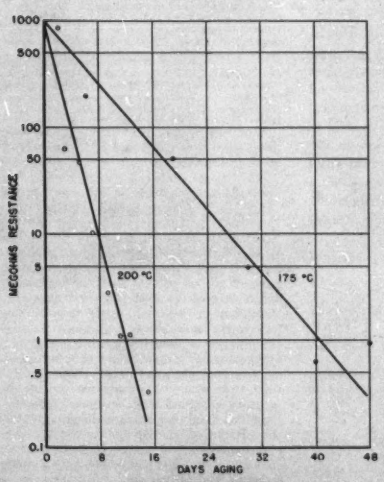
(d) Time to reach 20 per cent tensile strength of manila paper sealed in oil under nitrogen. (Data from AIEE transformer subcommittee on Co-ordination of Aging of Transformer Insulation)

(e) Time to reach 1 per cent original resistance of an organic varnish film (Westinghouse #7826-3) measured in salt water solution. (Data from L. E. Frost)

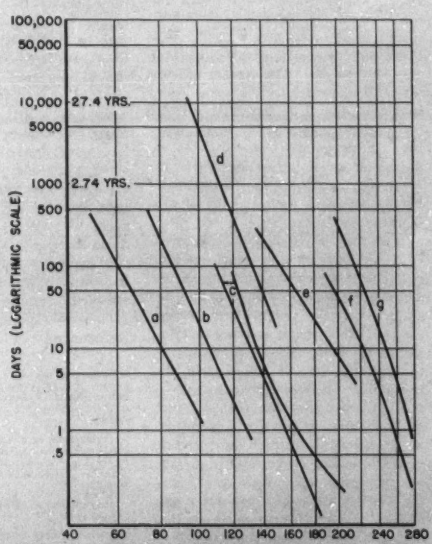
(f) American Society for Testing Materials flexible life of 990A silicone varnish film. (Data from R. Runk)

(g) Time to appearance of visible cracks in 990A silicone varnish film. (Data from R. Runk)

Figure 7. Decrease of insulation resistance (logarithmic scale) in salt water of varnish films with time of aging (dry) at elevated temperatures



STATIC LIFE TEST VARNISH FILM 7826-3
(LOGARITHMIC MEAN VALUES)



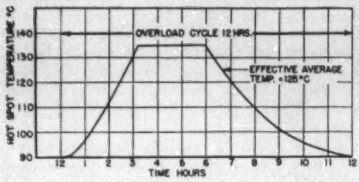


FIG. 9a

Figure 9. Integration of deterioration of insulation during a high temperature overload cycle.

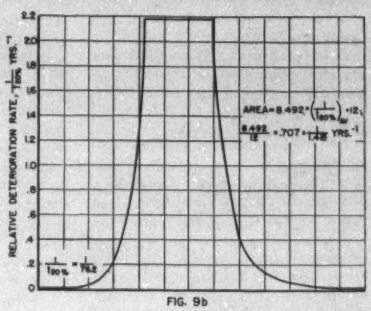


FIG. 9b

rary periods would shorten this.[9–12]

The first step in computing the effect of an overload cycle is to find the relative average rate of deterioration for the period of the cycle. This is easily done once the temperature-time curve is known Figure 9*a* and the life-temperature variation Figure 6 is obtained. From equation 6 it is seen that the rate of deterioration is inversely proportional to the time necessary to reach a particular condition of deterioration (the same for all temperatures). Therefore, to obtain the relative rate of deterioration at different temperatures, we merely take the reciprocal of the times to reach 20 per cent tensile strength, or whatever other end point is selected. These time values are taken from the upper line of Figure 6 and plotted corresponding to the temperature of Figure 9*a* to give a relative rate of deterioration curve of Figure 9*b* for the overload cycle. The average relative rate of deterioration for the overload cycle is readily obtained by integrating the area under curve 9*b* and dividing by the total time of the cycle. The average relative rate of deterioration for the sample overload cycle shown in Figure 9*a* is 76.2/1.415 = 53.8 times what it would have been at the normal temperature of 90 degrees centigrade. Therefore, this single cycle of 12 hours has subtracted 53.8 × 12 hours = 26.9 days from the life of the insulation (76 years).

If the duty of the transformer consists of a continued series of equal overload cycles with no temperature that can be considered normal, it is better to find the effective average temperature for the cycle and then simply consider that the transformer is operating continuously at that temperature. If, for example, the transformer operated repeatedly over a cycle of temperature like that of Figure 9*a*, the effective average temperature would be found in Figure 6 corresponding to the reciprocal of the relative average rate of deterioration $1/1.415$ years^{-1}, and the effective average temperature is 125 degrees. Under a condition of repeated operation over this temperature cycle, it would take 1.415 years for the tensile strength of the insulation to reach 20 per cent of its original value.

Summary

In this paper it is assumed that it is possible to select a physical condition of the insulation (an end point) which is considered hazardous to the operation of the electrical apparatus. It has been shown how, using generalized chemical reaction rate laws, it is possible to quantitatively describe the deterioration of the insulation to this selected end point. Use of the graphing methods outlined in this paper permits extrapolation of the results of short time experiments to longer periods of time and other temperatures with more confidence. The theory outlined here has been applied to experimental data on cellulose paper deterioration and other types of insulation deterioration tests. It is emphasized that close attention should be given to insuring that laboratory test conditions correspond closely to all conditions in actual electrical apparatus if such tests are to be used in predicting the life of insulation in the apparatus; also, a sufficient number of duplicate tests should be run to obtain a good average and information concerning the statistical variation from this average obtained.

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Discussion

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The author has approached this subject from the viewpoint of the chemist. I shall discuss it from the standpoint of those who apply data of this kind to the loading of transformers.

So far as the data given in the recent report by the AIEE transformer subcommittee on "Cooperating Aging Tests" are concerned these tests were not comprehensive enough to prove anything but the limited purpose for which they were made, namely, to settle the question of whether the tensile strength of insulation in oil protected from air decreases to, say, 40 per cent of the initial strength and then ceases to decrease any further for an indefinite period of time.

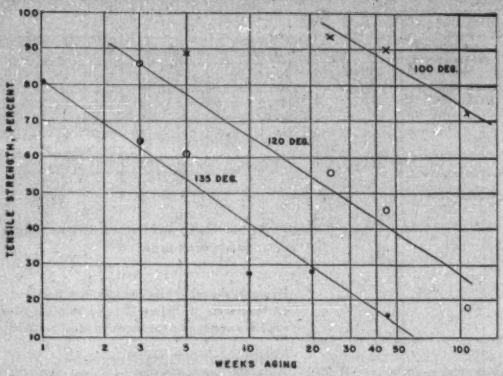


Figure 1. Manila paper with cloth and fuller board sealed in oil under nitrogen. (Test points taken from Figure 4 of Dakin's paper)

They proved that the material does not cease to deteriorate as had been contended by some investigators but continues to deteriorate at an unbroken rate until its life is gone. The test points are too few and scattered to establish any theory of aging.

The author's proposed method of plotting aging data differs in two ways from that generally used by previous investigators. First, he states that tensile strength should be plotted on a log scale and time on a linear scale, as shown in Figure 3 of the paper, instead of the reverse; and second, when plotting time versus temperature to produce a given per cent of initial strength, say 20 per cent, the time should be on the log scale while the temperature scale should be of a decreasing order proportional to the reciprocal of the absolute temperature (as shown in Figure 6 of the paper) instead of on degrees centigrade scale.

Referring to the first point, the method of plotting advocated by Dakin is not new. The AIEE data plotted in Figure 3 of the paper were plotted exactly the same way in the AIEE report. However, the reason for the choice of the scale used was convenience whereas Dakin wishes to establish it as a preferred method. The data in the AIEE report are too few to establish such a preference. Dakin's own data, shown in Figure 4 of the paper and replotted as a straight line in his Figure 5, definitely contradict that method of plotting, for in his Figure 5 the empirically determined ordinate scale is very different from the log of the tensile strength. In fact, I have taken those data and plotted them by the conventional method in Figure 1 of the discussion (strength versus log of time), and it will be seen that they fall as nearly on a straight line as they do in Dakin's Figure 5 (log of strength versus time).

Limited portions of data can frequently be plotted as a straight line by more than one method. When curves are plotted on linear co-ordinate paper, the portions of practical interest have the appearance of being asymptotic to the axes. However, the majority of published data covering wide ranges of time for different temperatures show that a straight line is obtained when tensile strength below approximately 80 per cent initial is plotted against log of time in the usual manner. Clark's voluminous data are excellent examples. We are usually interested only in strengths of 50 per cent and below.

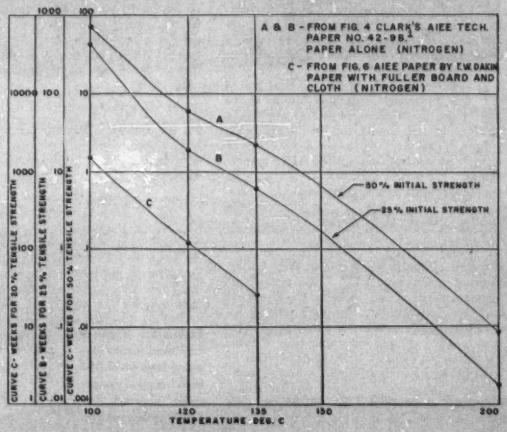
Referring now to the second point, namely, that a straight line is obtained on semilog paper if time is plotted on the log scale and the temperature scale is made proportional to the reciprocal of the absolute temperature as shown in the author's Figure 6. Dakin claims that Clark's data fall on a straight line when plotted this way, as shown on the bottom line of Figure 6 of this paper. But Clark's 100 degree test point is incorrectly plotted in Dakin's paper. It should have been at approximately 500 weeks instead of 95 weeks. The 135 and 120 degree points are plotted correctly. If all points are correctly plotted, the line is not straight as shown but has a decided bend.

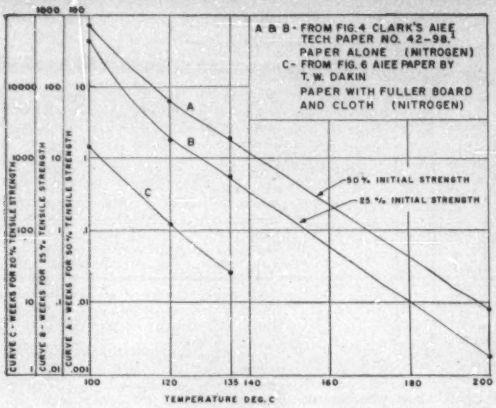
To see how Clark's data fall on the scale that Dakin suggests for plotting aging tests, I have plotted Figures 2 and 3 of the discussion for the 50 per cent and 25 per cent strengths for 100, 120, 135 and 200 degrees centigrade lines taken from Figure 4 of Clark's paper.[1] Figure 2 of the discussion shows the data plotted on semi log paper using the temperature scale suggested by Dakin. It will be seen that a straight line cannot be drawn through any three of the four test points. Figure 3 of the discussion shows the same data plotted in the usual way. The lines will be seen to be straight from 200 degrees down to 120 degrees and then to bend upward. The reason for the upward bend of the lines from 120 down to 100 degrees centigrade is seen in Figure 7 of Clark's paper which shows that the doubling of the life for a decrease of 8 degrees does not apply for temperatures below approximately 115 degrees centigrade. At approximately 100 degrees centigrade the doubling relation occurs for a change of approximately 5 degrees centigrade.

I was very much interested in Figure 9*a* of Dakin's paper in which he works out the effective average temperature of an over load that produces 135 degrees centigrade hot-spot for approximately 3 hours during a 12-hour period. To see how the effective average temperature, when derived by the method that I have used, compares with that obtained by Dakin's method I resolved the hot-spot curve shown in Figure 9a into a rectangle and triangle, and integrated these two areas, as shown in Figure 4 of the discussion. This method of integration shows that the effective average temperature when derived by the simpler method is 123.3 degrees centigrade, as compared with 125 degrees centigrade when worked out by Dakin's more elaborate method. The difference in the two answers is negligible in this case, but in another one we find the following very significant difference.

According to Dakin's aging data it would require 1.415 years (74 weeks) to reduce the tensile strength of paper (with some cloth and pressboard present) to 20 per cent when operated at an effective average tempera-

Figure 2. Temperature, degrees centigrade (reciprocal absolute temperature scale)





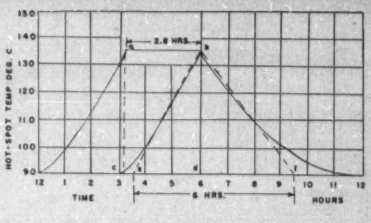


Figure 3 (left). Plot of Clark's and Dakin's data on standard semilog paper

Figure 4 (above). High temperature overload cycle shown in Figure 9a and solution for effective average temperature by usual method

Rectangular Area a, b, c, d — Units

$$P = te^{0.0865T} = 2.8e^{0.0865\times135} = 329{,}000$$

Triangular Area e, b, f

$$P = 6\left[\frac{e^{0.0865\times135} - e^{0.0865\times90}}{0.0865(135-90)}\right] = 177{,}000$$

Total = 506,000

If $506{,}000 = 12e^{0.0865\times T}$

$T = 123.3$ versus 125 degrees centigrade given in Figure 9a

P = aging units, $e = 2.718$,

T = maximum hot-spot, t = time

ture of 125 degrees centigrade. The data given in Figure 4 of Clark's paper show that it would require approximately 25 weeks or 1/3 the time. Here is a difference of 3 to 1. Which one is the more correct?

Recently I made some aging tests which check Clark's data very closely. In these tests strips of manila paper were inserted between turns of transformer-type coils having plenty of pressboard around them, as indicated in Figure 5 of the discussion. These coils were immersed in 25 to 40 degrees oil, heated by current to the temperatures of 195, 179, and 157 degrees centigrade as shown in left side of Figure 6 of the discussion. The temperature was observed by thermocouples placed against the paper strips. It will be seen that these aging tests, made under transformer conditions, check quite well Clark's test-tube data on paper alone. My 195 degree line, when proper allowance is made for the 5 degree difference in temperature, checks reasonably well Clark's 200 degree line. The two right-hand 135 degree lines extrapolated from my 179 and 195 degree test data, are in very close agreement with Clark's 135 degrees centigrade test data.

In conclusion, it is my opinion that Dakin has not presented sufficient evidence to justify the use of his more complicated ways of plotting and computing insulation aging values. The more comprehensive series of tests reported in Clark's 1942 AIEE paper[1] support the usual method of plotting the tensile strength on the linear scale and time on the log scale but do not support the method advocated by Dakin.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper emphasizes the need and value for the electrical engineer to consider chemistry theory in studies of deterioration of electrical insulation. The graphical representation of data, particularly in Figure 8 of the paper clearly indicate how widely different kinds of deterioration phenomena behave similarly as a function of temperature (although the amount of loss of life at a given temperature varies between the phenomena). It is implied in Dakin's paper, as well as in other similar literature, that the magnitude of deterioration caused by operation at different temperatures may be simply determined by adding the deteriorations at each of the operating temperatures that are indicated by the steady-temperature tests. For example, continuous laboratory tests might indicate that operation at 100 degrees centigrade for one year would cause X per cent deterioration and that operation at 150 degrees for two days on another piece of the same material would cause Y per cent deterioration, but some of our test data suggest that when such a combination of tests is made on one given sample, the resultant deterioration is considerably larger than X plus Y per cent.

I have recently arranged for further tests to check this finding. Oil-filled single-conductor cable is being used as the basic samples in the tests. The test setups are being varied to simulate conditions ranging from a sealed system free of air and moisture to a loosely-gasketed system, in order to obtain data applicable to different transformer types and to paper-insulated power cable.

T. W. Dakin: Montsinger and Halperin have raised some very pertinent points concerning the subject discussed in this paper. Montsinger has treated some particular points in some detail. In answer to Montsinger, it can be pointed out that in this paper a rational theoretical approach to the subject of insulation deterioration has been attempted, in contrast to the purely empirical approach with which Montsinger is familiar. Montsinger must admit that a chemical process is the reason for mechanical deterioration of the insulation. He, therefore, should not object to the application of chemical principles to this problem.

The author does not wish to defend any particular data either newly presented in this paper or obtained from the literature. Data which were available at the time the paper was written were used to illustrate the principles discussed. The tests sponsored by the AIEE transformer subcommittee on Co-operative Aging of Transformer Insulation seemed to have been conducted under fairly well controlled conditions, a feature not always present in the case of many aging tests which are made. It is difficult to fit any theory to widely divergent test results. Therefore, data obtained under well controlled conditions are necessary to

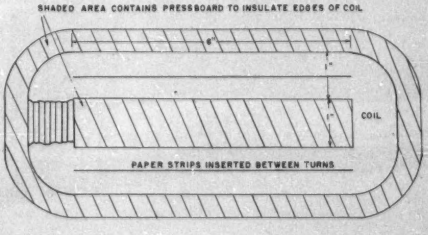


Figure 5. View of coil used for making aging tests on 0.003 inch manila paper strips inserted between turns as shown

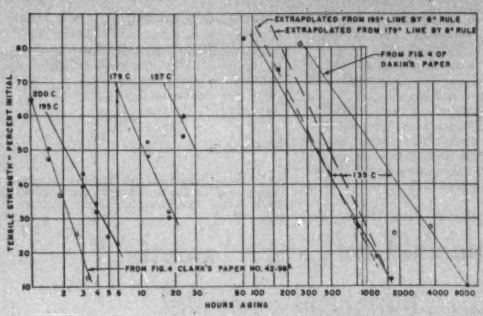


Figure 6. Comparison of aging of manila paper at 157, 179 and 195 degrees centigrade in transformer coils in oil (25 to 40 degrees centigrade) with Clark's and Dakin's data

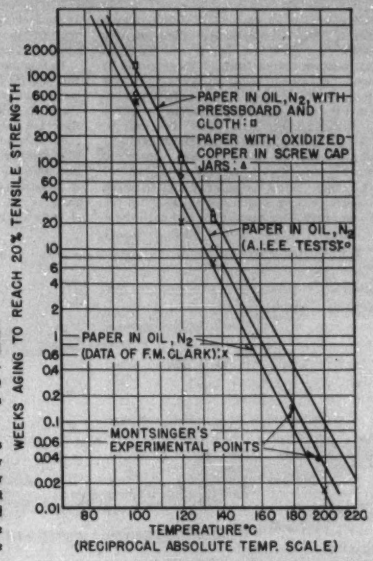


Figure 7. The time in weeks aging at elevated temperatures for manila paper to decrease under various conditions to 20 per cent of original tensile strength, plotted on a logarithmic scale versus the reciprocal absolute temperature

check any theory. It seems that Montsinger is unjustified in stating that the Figure 5 of the paper contradicts the method of plotting proposed by the author. The author did not say that all data should be plotted with the logarithm of the physical property (tensile strength) against the time. In the paper, the discussion following equation 3a pointed out that a more complicated function, rather than the logarithm, of the physical property would in many cases be proportional to the time, equation 4a.

The empirical method of plotting used by Clark in which he reverses the scales and plots the time logarithmically has the objection that it does not have a logical starting point at zero time. That method of plotting, although it has been used by Clark in several papers, should hardly be called conventional.

Montsinger is correct in saying that the 100 degrees centigrade test point taken from Clark's data was plotted incorrectly in Figure 6 of the paper. However, when it is plotted correctly, as Montsinger did in his discussion in Figure 2, together with the 200 degree centigrade test point, the four points fall on, or are symmetrically scattered on either side of, a straight line. This can be verified by drawing a straight line through the points of curves *A* and *B* of Montsinger's Figure 2. The data of Clark which give the time to reach 20 per cent of original tensile strength in oil under nitrogen are plotted in Figure 7 of this discussion. The line drawn through Clark's data with the 100 degrees centigrade test point plotted correctly, and the 200 degrees centigrade test point added then falls parallel to the other lines in the Figure 6 of the paper, which are reproduced in Figure 7 of this discussion. The parallelism of these lines indicates that the reaction mechanism of cellulose deterioration is probably the same under all the conditions given. The fact that they do not fall on top of each other indicates there is some other chemical substance than the cellulose which is affecting the reaction or entering into it, and which is present in different amounts in the different cases.

It seems that Montsinger is unfair when he draws a straight line through scattered points in his Figure 6, but does not draw a straight line in his Figure 2, but draws a curve to touch each point exactly. There seems to be a similar amount of scattering in both cases. In one case, Montsinger wanted to show a linear relation so he drew a straight line. In the other case, he did not want to show a straight line so he drew a curve.

In Figure 3 of Montsinger's discussion, the points decidedly do not fall symmetrically on either side of a straight line. The points show a decided trend with the lower temperature points indicating a curve upwards. This verifies, as do also the statements of Montsinger, the point the author establishes theoretically in the paper, that the temperature interval in which the reaction rate doubles decreases as the temperature decreases. It is, therefore, as even Montsinger admits, not possible to apply his 8 degree rule below 115 degrees centigrade, where the rule seems to change to about a 5 degree rule. Compare this with Figure 1 of the paper. Montsinger may find upon investigating the origin of the 8 and 10 degree rules that they came originally from chemists who many years ago proposed them as rough approximations of the temperature coefficients of the chemical reaction rates. Since Montsinger has accepted and used these rough approximations proposed by chemists, he should not object to using the more exact expressions from which his 8–10 degree rule was borrowed.

Montsinger it seems is also unjustified in referring to the integration of the curve of Figure 9*b* of the paper as being more elaborate. The integration of this curve can also be done roughly by the use of triangles and rectangles as Montsinger has done. There is no mathematical calculation at all involved in converting from Figure 9*a* to 9*b* by the procedure given in the paper, and upon close examination, it should be seen that the method given in the paper is actually simpler than Montsinger's equations which involve fractional exponents. Familiarity, of course, makes one method or the other seem simpler.

There is no problem at all in answering Montsinger's question: "Which is the more correct?" Clark's data or the data reported in the paper, which was obtained by McCulloch. The answer is that both are correct for the conditions under which the respective aging tests were run. The conditions were somewhat different in the two cases, and, therefore, the time to deteriorate may be and is different.

Montsinger's experiments, which he reports in his discussion, are very interesting and more of that type of testing should be encouraged. No matter how the data are treated after it is obtained, it cannot be emphasized too much that the testing conditions should reproduce as closely as possible the conditions in the apparatus. On this point, almost every one should agree. Peculiarly enough, the data of Montsinger obtained with transformer conditions seem to agree better, upon close examination, with the extrapolated AIEE tests data represented in this paper than with the test results of Clark. Figure 7 of this discussion is the same as Figure 6 of the paper, with the exception that the 100 degrees centigrade test point of Clark's data has been replotted more correctly, his 200 degrees centigrade test point added, and the other lines extrapolated to 200 degrees centigrade. In spite of this rather close agreement of the extrapolated lines with Montsinger's experimental points, it should not be regarded as an unqualified verification of the theory, inasmuch as the experimental conditions were not entirely the same in the case of the AIEE tests as in Montsinger's tests.

In Halperin's discussion, he has raised a good point, concerning the additivity of

deterioration effects at different temperatures. Most experimental data of any accuracy reported to date have been taken at constant temperatures. There should be well controlled tests of aging run over temperature cycles in which the temperature is measured accurately and the other conditions controlled. It is true that in this paper it is assumed that the amounts of deterioration at different temperatures are additive in a sense. This is true of chemical reactions. Whether or not it applies in the complex case of electrical insulation has yet to be proved satisfactorily. But until there is good evidence to the contrary, the best we can do is to assume that additivity applies.

In summary, it should be restated that in this paper it has been attempted to set down some principles which should apply in the study of insulation deterioration, and to show by example how they apply to some existing aging data. The author admits that extrapolation from data is dangerous, but in lieu of no information at all, it is best to extrapolate by recognized theories than to guess.

Tests to confirm or deny this or any theory should be encouraged. The methods of plotting aging data described in this paper have a theoretical basis and should therefore be preferred over empirical methods of plotting or extrapolating data in cases where the theory has been shown to apply correctly.

Reference

1. Factors Affecting the Mechanical Deterioration of Cellulose Insulation, F. M. Clark. AIEE Transactions, volume 61, 1942, pages 742–9.

"Terratex"—A Thin Flexible Inorganic Insulation

T. R. WALTERS
MEMBER AIEE

Synopsis: A limited number of inorganic insulations has been available to the designer, as compared with the variety of organic materials on the market today. The development of terratex, a thin, totally inorganic paper-like sheet, fills a gap in the inorganic insulation field. Laboratory tests and four years of service experience indicate that its excellent high-temperature characteristics and other properties may be utilized wherever it is advantageous to use inorganic insulation.

ONE of the major factors which has limited the output rating of electric equipment has been the deterioration of organic insulations at high temperatures. To aid in obtaining higher ratings from the same size equipment, terratex, an inorganic insulation, has been developed to take the place of certain commonly used organic sheet insulations.

To fill the every day needs of the designer, a wide variety of organic sheets and tapes has been developed. The sheets are usually cellulosic papers and pressboards, whereas cellulosic fibrous materials take the form of yarns or woven tapes and cloths. Paper and pressboard are used as widely as possible because of their relatively low cost and excellent dielectric properties which result in good utilization of space. Yarns and fabrics are characterized by higher costs and poorer dielectric properties, consequently, they are used only where their better physical properties are required. Years of operating experience and laboratory tests have resulted in standardizing on a maximum temperature of 105 degrees centigrade for organic insulations, which are listed[1] as class *A*.

In the class *B* insulation field,[1] with a corresponding maximum temperature limit of 130 degrees centigrade, the designer is limited by the small number of useful materials. Mica sheets and tapes are expensive and often difficult to use. Commercial asbestos papers usually contain about 15 per cent of cotton fibers and starch, and are thick and weak dielectrically. Fiber glass yarns and cloths are excellent mechanically and thermally, but are fairly expensive and in themselves only spacers depending on varnish filling for dielectric strength. There has been no inorganic counterpart of the relatively low-cost high-dielectric-strength organic papers.

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T. R. Walters is with the electrical research section, works laboratory, General Electric Company, Pittsfield, Mass.

Figure 1. Terratex tape

Development of Terratex

Consideration of the problem of providing an inorganic counterpart of organic paper led to the conclusion that a flexible inorganic sheet would require a fibrous base similar to that of the cellulosic fibers used in organic papers. Two inorganic fibers were known—asbestos and glass. Neither can be "hydrated" in the way cellulose fibers are in paper making.

As glass fiber is better electrically than natural asbestos fiber, attention was given to it first. After many trials, a mineral filler and binder was developed to use with it, and a method developed to make a continuous sheet of this combination which bore a strong resemblance to paper. This sheet could be made in thicknesses of a few thousandths of an inch, had high tensile strength, and good dielectric strength. However, it lacked sufficient flexibility to be wrapped on wire.

Asbestos fibers are more flexible than glass, but they contain many impurities and conducting particles which limit their use as dielectrics. Means were found for preparing the fiber to much smaller dimensions, and for the removal of impurities and conducting particles to a degree not considered commercially feasible in the past. Means were also found for combining this purified fiber with the mineral binder and filler by a special paper-making process on fairly standard paper-

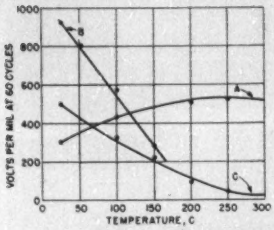

Figure 2. Breakdown strength of untreated teratex and some other inorganic insulations

A—Untreated teratex, two sheets—0.006 inch—0.020 inch total thickness
B—Pyrex glass—0.030 inch
C—Porcelain—0.055 inch

making machinery. The resulting sheet (Figure 1), which has found a number of uses in electric apparatus, is thin, flexible, 100 per cent inorganic, and very similar in appearance to cellulosic paper. All of the following data and uses are based on this asbestos-type material.

Properties of the Sheet—Untreated

This new sheet has been produced as thin as one mil. Regular thicknesses are 1.5, 2, 3, 4, 5, 8, and 10 mils. So far, the greatest use has been for wire covering with the 3-mil material. During the past three years, an average of 66 pilot-plant paper mill runs per year have been made of this thickness material for this purpose. The range and averages of thickness, tensile strength, and dielectric strength for the three years are shown in Table I. Five tests were made to obtain an average for each lot; the dielectric strength tests being made at prevailing room conditions. Data for 1947 are incomplete.

At present, the physical properties of tear and tensile strength, while not initially as high as those of commercial asbestos papers, are sufficiently high for effective use and extremely stable with time at high temperature. In aging tests such as are widely used on class *A* insulations, where the decrease in tensile strength is measured against time at various temperatures, no decrease is found in tests of many months duration. At 175 degrees centigrade, a temperature which reduces cellulose to a useless cinder in a matter of hours, terratex retained its original tensile strength after 710 days. Commercial asbestos paper containing the usual percentage of organic material had dropped 50 per cent to equal the strength of terratex in ten days and had lost 90 per cent of its initial strength in 87 days.

Besides being unaffected by long time exposure to the high temperatures it might be subjected to in electric apparatus, terratex is completely noninflammable. It can be brought to a bright red heat (850–950 degrees centigrade) in a Bunsen burner flame, with neither ignition nor melting of the sheet. In fact, it retains a dielectric strength in the order of 100 volts per mil at 800 degrees centigrade.

At room temperature the dielectric strength of this sheet is in the order of 300 volts per mil. Because of the drying out of absorbed water, this value increases to about 500 volts per mil at 300 degrees centigrade and gradually drops back to 300 volts per mil at 500 degrees centigrade. At this and higher temperatures the material becomes brittle in much the same way that "Muscovite" or white mica does, due to loss of water of crystallization. For these tests, two sheets of 3-, 5-, or 10-mil terratex were placed between 5/8-inch-diameter platinum electrodes. The bottom electrode was insulated by several more sheets placed over a flat U of resistance wire used as a strip heater. This assembly was set into the center of two magnesia blocks, with a thermocouple to measure temperature. Regular American Society for Testing Materials[2] 60-cycle 1-minute step-by-step tests were used.

It is of interest to note that this trend of increasing dielectric strength with increasing temperature is not always exhibited by inorganic insulations. Figure 2 shows, for example, that such well-known dielectrics as porcelain and Pyrex glass become quite conducting at temperatures[3] above 150 degrees centigrade. These data on glass are verified by Littleton and Morey[3] who show about the same reduction in dielectric strength with temperature over the same range reported here. Thus, while they may be nonaging, their use as dielectrics at the higher temperatures is definitely limited.

Properties of Treated Terratex

Although much better from both a thermal-life and dielectric-strength standpoint than commercial asbestos paper, terratex at present is initially no stronger physically, as noted earlier. This, coupled with the fact that this sheet can be made much thinner than commercial asbestos papers, makes the actual tensile strength in pounds per inch of width quite low. Since all class *B* coils are given some type of varnish or compound treatment eventually for structural and moisture retardant reasons, the sheet is generally pretreated to facilitate handling. This treatment increases the tensile strength of the 3-mil wire-covering type of terratex from an average strength of 300 pounds per square inch to a minimum of 4,000 pounds per square inch, which is more than ample for high-speed wire covering. The varnish used can be one which previous experience has shown to be good for high temperatures, although no varnish available today has heat resistance equal to that of terratex sheet.

Varnished, the sheet naturally takes on some of the properties of the treating material. Dielectric strength tests have been made on alkyd, asphalt, phenolic, copolymer, and silicone treated sheets. The results of three of these, on "burn-out" dielectric tests run to the same

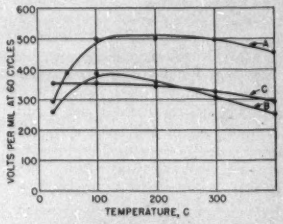

Figure 3. Breakdown strength of terratex with different resin treatments

A—Silicone treated
B—Alkyd treated
C—Copolymer treated, two sheets—0.007 inch—0.022 inch total thickness

Table I. Range of thickness, tensile strength, and dielectric strength variation on nominal 3-mill sheet "as made"

Year	No. of Runs	Thickness, Mils			Tensile, Lb Per Sq In.			Dielectric Strength, V Per Mil		
		Max	Min	Avg	Max	Min	Avg	Max	Min	Avg
1945	62	4.0	2.5	3.25	404	227	297	400	117*	304
1946	66	4.0	2.9	3.3	754	214	333	403	167*	313
1947	47**	4.0	2.7	3.3	413	214	332	407	185*	285

* Single lot—remainder well above 200 volts per mil.
** Not for complete year.

temperatures as the all-inorganic untreated sheet are shown on Figure 3. A minimum of varnish was used in treating these sheets. They were barely saturated, so that no surface film existed, as it was desired to limit dependence on organic materials to a minimum.

Treated cellulosic sheets carbonize and disintegrate when subjected to overtemperatures for a length of time. Terratex has been found to retain its naturally high initial dielectric strength when the treating compound is "cooked" out of it, while inorganic fabrics under the same conditions become merely "spacers" of low dielectric strength.

With loss of varnish, all flexible inorganic sheets including mica lose their initial moisture resistance. Table II shows the effect of moisture on lightly treated sheets of 3-mil terratex. Designers should note that the effect of water is greater on terratex than on the more familiar kraft paper. The data also indicate that a better varnish will result in improved properties.

To obtain these data, the dielectric strength was measured at 60 cycles between 2-inch-diameter electrodes with edges rounded to 1/4-inch radius. Breakdown was taken on single sheets in air at temperatures indicated, with continuous rise of voltage so as not to dry out the "wet" samples. A "25 degrees wet" measurement was on sheets which were immersed in tap water for 24 hours then removed, blotted dry, and tested immediately. Each value is the average of five measurements.

Methods of Use

So far all manufacture and treatment of this sheet has been on a pilot-plant basis, so that its uses have been limited. The most important use has been on large magnet wire. Other uses are as layer insulation, mica backing, and a mica substitute.

Magnet Wire

Pretreated 3-mil tapes have been applied to a wide range of rectangular wire sizes, from 90 mils square up to large flat conductors. They were applied in two or more butt-wrapped layers in much the same way that paper tapes are used for class *A* apparatus, except that they were "bonded" by applying an adhesive to the wire as the tape was wrapped on. In cases where the coil winding operation involved severe abrasion, an outer wrap of glass fiber was applied and varnished, corresponding to the cotton yarn that is often used over paper covered wires for mechanical protection.

Figure 4. Wire sample for thermal aging test

With a 12-mil build of insulation (six mils on a side) this terratex wire has a breakdown strength of 160 volts per mil or better. This compares with a strength of 60–80 volts per mil for felted asbestos wire, and of 80–100 volts per mil for varnished glass yarn wire, of the same insulation thickness tested under the same conditions. The copolymer treated material was used for the wire insulation to obtain a good balance between electrical, thermal, and physical properties.

For these tests, samples of wire six inches long were used; each individual wire being covered for four inches with lead shot. A 60-cycle potential was applied between the conductor and the shot and gradually increased to puncture.

Additional laboratory tests have been made on pairs of each of these three wire insulations. These data are shown later in Table III under the discussion of thermal aging. The results obtained in this manner are higher than the above figures, but bear the same relation to each other. The copolymer treating material is an elastomer, and imparts a degree of stretch and tensile strength to the tape that makes it well adapted to wire covering. Such insulation yields when the wire is bent, instead of rupturing the way unstretching cellulosic papers do.

Since the copolymer is also thermoplastic, consideration was given to what might happen to wires under short-circuit conditions, where high temperatures and high mechanical stresses occur simultaneously. Stacks of rectangular wire three high and three wide were tied together and put through factory varnish treatment. They were prepared for rapid connection to a circuit applying 110 volts between adjacent turns, and then preheated to 175 degrees centigrade. When fully up to temperature, they were taken from the oven, placed immediately between the platens of an hydraulic press which were also at 175 degrees centigrade, and the connections made. The pressure was then increased gradually until an electrical failure developed. This occurred at a pressure of 1,000 pounds per square inch, which was considered more than adequate.

Layer Insulations

Depending on the voltage between layers, size of coil, and other factors, any one of the available thicknesses of terratex may be used as a layer insulation for high-temperature operation. The thinner sheets generally will require pretreatment to facilitate handling, while the 5-mil or thicker sheets may be either presaturated or used untreated and impregnated during the coil treatment.

In either case, this new sheet makes it possible through its inherent high dielectric strength to obtain a high space factor with minimum dependence on varnish. If it is desired both to saturate and to coat

Table II. Effect of moisture on the short-time 60-cycle dielectric strength of lightly treated 3-mil terratex

Paper	Varnish	Thickness, In.	Dielectric Strength, V Per Mil			
			25 C	100 C	150 C	25 C Wet
Kraft	Phenolic	0.0035	372	126	126	154
Terratex	Phenolic	0.0040		245	222	93
Terratex	Asphaltic	0.0035	421	448	368	195

Figure 5. Terratex insulated coils for 500-ampere air-cooled arc-welding transformer

the sheet with varnish, as some cellulosic papers are, higher dielectric strengths can be obtained. For example, a typical saturated 3-mil sheet has a dielectric strength of 400 volts per mil. Saturated and coated, the same sheet will have a strength of 600 volts per mil or more.

Mica Backing

Composite sheets of insulation often are used to fill the particular requirements of certain types of apparatus. Flexible mica generally has a sheet of thin organic paper pasted to each side to give it tensile strength and to hold the mica flakes in position. Despite its mica content, such a sheet is not satisfactory for temperatures so high that the cellulose disappears in time leaving a loose structure.

To obviate this difficulty, thin varnished cloths of glass fiber have been used in place of the paper. This results in a sheet with excellent thermal life and physical properties, but of very high cost.

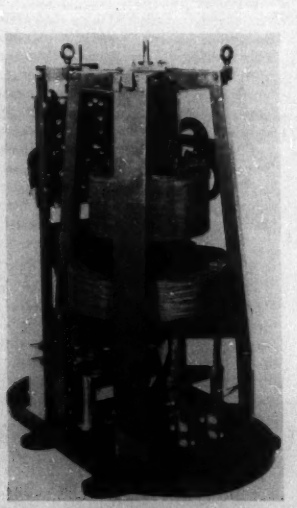

Figure 6. Terratex insulated 500-ampere air-cooled arc-welding transformer

Replacement of one of the two 2-mil varnished glass cloths with a 1.5-mil varnished terratex on a flexible mica sheet which totaled 10 mils, resulted in a composite of equal thermal life, a slightly higher and more uniform dielectric strength, ample tensile strength, and lower cost. A quantity of such sheets was used successfully during the war in certain very compact rotating equipment for aircraft. To save weight, this equipment was designed to operate at high temperature.

Mica Substitute

Nominal 5-mil flexible mica sheets, particularly of the machine-made type, are quite variable in thickness (2–12 mils). At the thin spots the breakdown voltage is quite low. In comparison, 5-mil varnished terratex is a continuous, homogeneous class *B* sheet with a thickness variation of not more than 0.5 mil, and an equally uniform dielectric strength of about 400 volts per mil.

Comparative burnout tests with overcurrent have been made on windings with (*a*) organic-backed mica and (*b*) terratex used as insulation between layers. In time, the organic backing and the adhesive disappeared, leaving only the mica insulation. Under the same conditions, the terratex remained in position, providing practically the initial insulation.

Thermal Stability of Treated Terratex

Again following the conventional aging test for class *A* insulations, a large number of strips representing 30 combinations of different varnishes on terratex and on commercial asbestos paper were aged in a 175-degree-centigrade oven. Periodically, several strips of each type were removed from the oven and tested for tensile strength. From these data it was concluded that

1. In general, the use of an organic material as an integral part of the asbestos sheet results in a material of short life.
2. After varnishing, such materials have a three to five times better life span.
3. When a totally inorganic paper such as terratex is varnished, its life span is at least six times that of a varnished asbestos paper containing organic material as an integral part.

When the tensile strengths of most of the varnished terratex samples leveled off at the same value as that obtained on untreated terratex, it was concluded that this type of test had far less meaning on inorganic base materials than on organic insulations. Hence, a test more nearly representative of conditions of use was adopted in which the insulation was

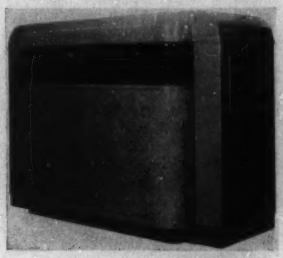

Figure 7. Terratex insulated dry-type transformer for load center unit substation

tested on wire, as layer insulation, and as spacers. As this development has progressed furthest in its application to wire insulating, most of these thermal aging data available at present relate to wire.

The samples consisted of pairs of 0.095-by-0.295-inch wire, insulated to 0.012-inch increase in dimensions with the three types of material under comparison. These were glass fiber, felted asbestos, and terratex. The flat sides of two 9-inch lengths of wire were held tightly together over the middle six inches with glass-fiber tying cord. The ends were flared to prevent flashover, and the samples were factory treated in phenolic varnish (see Figure 4).

On the assumption that loss of moisture protection was a limiting factor in the use of open dry-type apparatus, it was decided to use power factor measurements after exposure to high humidity as a criterion in judging the useful life. After measuring the initial power factor and dielectric strength between the conductors of several pairs, five sets of each type of wire were placed in a circulating air oven running at 175 degrees centigrade. At about

5-day intervals, the samples all were removed from the oven, cooled to room temperature, and placed in a humidifier operating at a 90 per cent relative humidity at 90 degrees Fahrenheit. After 24 hours the power factor was measured, and the samples returned to the oven. This cycle was repeated until a high power factor was obtained. The "break" in the power factor versus time curve was quite sharp in each instance. Also, it occurred at very nearly the same time on all samples of the same type. Dielectric tests were again made at this end point. (See Table III.)

In this series of tests the terratex insulated wire had double the breakdown strength of the other insulation of equal thermal life. This ratio was maintained after both had dropped in value due to thermal deterioration of the varnish.

Use in Transformers

To gain experience with this new material, limited production use of it has been made. Starting in 1943, a number of arc-welding transformers (see Figures 5 and 6) have been built each year using terratex insulated wire. In addition, a smaller number of dry-type load center unit substation transformers (see Figure 7) of much higher voltage rating, have been built over the same time period. No service failures have yet been encountered.

Table III. Dielectric properties of wire samples aged at 175 degrees centigrade—0.012-inch insulation

Sample	Per Cent Power Factor	Kv Breakdown at Room Temperature	V Per Mil
Double glass + phenolic varnish—Initial	3.0	2.6	216
End (51 days)	87.3	1.7	141
Felted asbestos + phenolic varnish—Initial	23.0	2.0	167
End (22 days)	96.0	2.0	167
Double Terratex + phenolic varnish—Initial	17.2	5.2	433
End (50 days)	88.9	3.4	283

Conclusions

1. A versatile new type of dielectric has been developed which fills a gap in the field of high temperature electrical insulation.

2. This flexible inorganic paper-like sheet is, by virtue of its method of manufacture, potentially less costly than the few other flexible high temperature insulations available at present.

3. This sheet possesses an unusual combination of dielectric strength, thermal life, and incombustibility.

4. Utilization of these properties by the designer can result either in improved space factor or a higher factor of safety.

5. Service experience in certain types of electric apparatus has to date shown no defects.

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No Discussion

Electronic Circuits of a Supersonic Reflectoscope

RALPH B. DeLANO, JR.
ASSOCIATE AIEE

THE SUPERSONIC reflectoscope is a device for the nondestructive testing and measuring of solid parts. It operates by sending pulses of supersonic energy into the material under test and measuring the length of time it takes these pulses to penetrate the material, reflect from the opposite side or an internal defect, and return to the sending point. Reflections are caused by the abrupt changes in elastic properties or density existing at defects and boundaries. A defect having a dimension greater than one-eighth inch when projected on a plane perpendicular to the axis of the supersonic beam may be readily located when ten feet from the surface, but the dimension of the defect in a direction parallel to the axis of the beam may be very small—in fact, the two faces may be in intimate contact.

Such an instrument has many practical uses. Fatigue cracks in axles, shafts, crank pins, and other metal parts may be found without disassembling the machine, provided the end of the part to be tested is accessible. Hydrogen ruptures in steel billets, laminations in plates, and defective welds are also among the defects which may be detected with the supersonic reflectoscope. The use of a reflectoscope in testing the shaft of a small generator is shown in Figure 1.

Figure 2 shows a typical reflectogram. The initial pulse appears at the left of the cathode ray tube trace, and at the right is seen the reflection from the end of the shaft. The reflection in the center is from a defect. Its distance from the surface may be obtained by counting the number of square wave cycles appearing between the pulse and the reflection from the defect.

One of the important electronic features of this device, as developed by Floyd A. Firestone, is the fact that a short duty cycle is employed and this permits operation with a-c power supplied directly to each tube without the use of any rectifiers. This paper gives a discussion of the circuits in the instrument and their operation with raw a-c power.

Functions of the Circuits

The electronic circuits of this reflectoscope may be divided into the following sections—sweep, delay, pulse generator and crystal or searching unit, main amplifier, and marker circuit.

Figure 3 shows the relationship between these sections. The sweep, which is synchronized to the supply frequency, starts the cycle of events and generates a voltage which is used to move the cathode ray horizontally across the face of the tube. The sweep also provides a synchronizing signal to start the marker circuit which puts distance markers on the tube face. A second synchronizing signal from the sweep circuit trips a delay circuit. This delay circuit in turn fires the tube which generates the pulse. The pulse is impressed across the crystal and causes the latter to generate a supersonic wave train in the part being tested. This pulse is also impressed on the amplifier whose output is connected to the vertical plate of the cathode ray tube. Reflections of this wave train strike the crystal where they are converted back to electrical pulses and impressed across the input of the amplifier. The output of the amplifier being connected to the vertical plate of the cathode ray tube, the time interval between the pulse and the reflections can be measured on the face of the tube. A circuit diagram of the instrument is shown in Figure 4.

Sweep

The sweep is generated by an inert gas thyratron tube, V_9, discharging capacitors C_2 and C_3. The gas tube is synchronized to the power supply frequency by having a sine wave of the supply voltage reduce the grid bias voltage to the critical value. Variation of the magnitude of the grid signal by means of potentiometer P_6 changes the point in the 60-cycle voltage at which the tube fires. Capacitor C_2 must discharge through potentiometers P_2 and P_3 as well as through the tube. Potentiometer P_2 is ten times the value of potentiometer P_3 and consequently controls the discharge time of capacitor C_2. A small portion of the discharge voltage is selected by potentiometer P_3 and applied to the grid of V_{10}. The output of V_{10} is applied directly to one horizontally deflecting plate of the cathode ray tube and through tube V_{11} to the other deflecting plate. Consequently, potentiometer P_2 controls the time required for the cathode ray tube spot to sweep across the screen, and for a given value of potentiometer P_2 the other potentiometer P_3, controls the magnitude of the sweep.

The general shape of the sweep voltage is shown in Figure 5. The cathode ray tube spot is normally biased off the right side of the screen, and the sweep voltage moves rapidly across the screen to the left side and then back at a rate determined by the setting of the sweep time potentiometer as previously explained. If the thyratron were not kept fired during the sweep and for some time thereafter, the sweep would be interrupted and the cathode ray tube spot would move to the left again. This is prevented by inserting a small resistor between the plate of the thyratron and the capacitors C_2 and C_3.

One of the advantages of this type of

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RALPH B. DELANO, JR., is research engineer with Sperry Products, Inc., Hoboken, N. J.

Figure 1. Testing the shaft of a small generator with a supersonic reflectoscope

sweep circuit is that by adjusting potentiometer P_3 so that a small value of resistance is in the circuit and increasing the value of the other potentiometer, P_5, the sweep piles up at the left side of the screen and reflections further along at the right may be seen on a more expanded scale than would normally be provided by an exponential or even a linear sweep.

When testing for defects near the surface, the exponential sweep possesses a distinct advantage over the linear sweep in that it provides good resolution on the screen for the area being tested and yet enables one to observe the back reflection. The back reflection provides a continuous check on the amount of energy being transmitted into the part under test, and this is important because occasionally surface irregularities cut down the amount of energy entering the part. Also some types of defects or a large number of small defects are more readily found by a diminution of the back reflection rather than a distinct reflection from the flaw itself.

Some loss in the brilliancy of the cathode ray tube trace is caused by the choice of a repetition frequency as low as 60 cycles per second. If an appreciably higher repetition frequency were used, however, material having low absorption would not attenuate the supersonic energy sufficiently between pulses. As a result, reflections of the previous wave train would appear on following traces and might be mistaken for defects.

A repetition frequency near 60 cycles being desirable for other considerations, it is advantageous to have it synchronized with the power line to prevent beat phenomena from causing jitter on the face of the cathode ray tube. Another advantage of synchronizing with the supply line frequency is that it permits the electronic tubes to be operated with a-c power, as will be discussed later.

Delay Circuit

An adjustable delay between the start of the sweep and the generation of the pulse is necessary with this type of sweep circuit because the time of the forward trace increases as the sweep time is increased. When a gas tube of the type used in this instrument is fired, a sharp positive jump in grid voltage occurs at the same time as the drop in plate voltage. This grid signal is differentiated and used as a trigger signal for a flip-flop circuit. The output of the latter circuit is differentiated again, yielding a negative pulse whose delay with respect to the input signal may be varied by adjusting potentiometer P_4. This pulse is inverted and amplified by tube V_{13}, whereupon it is applied to the grid of the pulse tube V_1, causing it to fire. In this manner the sweep appears on the cathode ray tube before the pulse, and the latter may be moved back and forth along the sweep as required.

Pulse Generator

A 1,000-volt exponentially damped pulse is generated when the gas tube V_1, fired by a signal from the preceding tube, discharges the capacitor connected across it and shock excites the tuned circuit L_1 C_1. A series potentiometer, P_1, controls the decay and magnitude of the pulse. The frequency of the pulse is the same as the supersonic testing frequency, and normal testing frequencies are 1/2, 1, $2^1/_4$, and 5 megacycles per second, although a continuous coverage in overlapping ranges is provided by the variable capacitor and the switch which selects the appropriate inductance. It also has been found desirable to switch the discharge capacitor when changing the pulse inductance, a smaller capacitor being used as the frequency is increased.

The pulse voltage appearing across the inductor and potentiometer is transmitted by a cable to the searching unit. This unit is an X-cut, piezoelectric quartz plate cemented to a bakelite button so that it may be held in the hand against the part being tested. The crystal is one-half wave length thick and therefore resonates at the pulse frequency. It vibrates in a piston mode and transmits these vibrations through a film of oil into the part being tested as a supersonic wave train. This wave train is reflected by the boundaries of the part being tested or by inhomogeneities therein, and upon striking the crystal it is reconverted into electrical energy.

Intermediate Frequency Amplifier

The pulses generated by the reflections are of the order of a few millivolts and must be amplified before they can be displayed on a cathode ray tube. They are mixed with a voltage generated by the local oscillator tube V_2 and its associated circuit and then amplified by the intermediate frequency amplifier comprising tubes V_3 through V_8. The amplifier is of the broad-band type, amplifying frequencies between 9 and 12 megacycles, but the output transformers are arranged so that the windings may be switched, giving an output peaked at 11 megacycles or tunably peaked and adjustable between 8.75 and 11 megacycles, as well as a broad output. The amplifier gain is much greater with the narrow band outputs, and these are used when sensitivity without high resolution is required. The amplifier has four cascaded reject circuits tuned to a frequency of 8.25 megacycles. When operating at a test frequency of 1/2, 1, or $2^1/_4$ megacycles, the local oscillator is set on its low range and tuned to 8.25 megacycles. The tunable output is normally used and the variable capacitors are set to resonate one of the output transformers at the sum of the local oscillator and testing frequencies. When testing at 5 megacycles the high range of the local oscillator is used, the oscillator being set at 16 megacycles, and either peaked or tunable peaked output is normally used. In either case the frequency

passing through the amplifier is the difference between the local oscillator and the testing frequency, namely, 11 megacycles. The output of this amplifier is connected to the upper deflecting plate of the cathode ray tube.

Marker Circuit

In order to accurately locate defects it is necessary to have time marks on the sweep. In this reflectoscope the time marks consist of square waves imposed as vertical deflections of the sweep trace. The marks are generated by the multivibrator circuit and its associated tubes V_{14} and V_{15}. The frequency of the square waves is adjustable by means of the ganged capacitors C_4 and C_5 so that one cycle of the square wave may be made to represent any distance from one inch to two feet in aluminum. Small adjustable capacitors are connected in parallel with the main capacitor to adjust the frequency range and balance of the circuit. A modified cathode follower circuit is used to isolate the multivibrator from the capacitance of the lower cathode ray tube deflection plate plus that of the connecting cable and to clip the square waves to improve their shape.

The multivibrator is synchronized with the sweep by preventing tube V_{14} from conducting and therefore making the circuit inoperative for a predetermined interval after the sweep has begun. This is accomplished by applying a negative bias from potentiometer P_5 through the tube V_{18} to the grid of tube V_{14}, preventing it from conducting. It should be noted that because of the large negative voltage in the cathode-to-ground circuit of tube V_{16}, the plate of this tube is negative with respect to ground but positive with respect to the cathode, thereby causing it to conduct current. When the gas tube V_8 fires, the negative pulse occurring at its plate is applied to the grid of tube V_{16}, driving it beyond cutoff. This action starts the multivibrator by removing the bias from tube V_{14}. The square waves may be moved along the sweep by adjusting the bias applied by potentiometer P_5.

Operation With Raw A-C Power

There are no rectifiers in this instrument since all the tubes have alternating voltage applied to each electrode that requires an operating voltage. The characteristics of the instrument which make this possible are a duty cycle which in short with respect to the period of the supply voltage and a repetition rate synchronized to the power supply frequency. Furthermore, it is relatively easy to separate the component of the plate voltage due to the alternating supply voltage from that component which is the response to the applied grid signal.

Figure 2. Reflectogram showing flaw

The length of the duty cycle depends on the length of the longest part to be tested and the velocity of waves in that part. Longitudinal supersonic waves travel in steel with a velocity of 229,000 inches per second. The velocity in most other metals is not more than 40 per cent less than the velocity in steel. The time required for a supersonic wave train to travel down a 30-foot bar of steel and reflect back to the crystal is only slightly over 1/360th second. Since there are few applications in which it would be desirable to test a piece longer than 30 feet, the instrument is at most required to operate for only one-sixth of the time interval between pulses. The remainder of the time is provided to allow the supersonic energy in the material to be absorbed.

Figure 6 shows the duty cycle symmetrically impressed on the peak of the positive alternation of the supply voltage. From this figure it may be seen that the 60-cycle supply voltage varies by only 13.4 per cent during the maximum duty cycle of the instrument. Most electronic circuits are capable of functioning normally over such a voltage change when this change is of a slow period relative to the desired signals in the circuits. When such is not the case, the problems parallel those of a direct-coupled amplifier wherein it is difficult to prevent the operating plate voltage of one tube from reaching the grid of the next without losing part of the amplified voltage or introducing instability.

The operating frequency of the circuits in the pulse generator and amplifier is so much higher than the power supply frequency that small coupling capacitors are sufficient to separate the two. Decoupling resistors and capacitors are used in the plate, screen, and grid circuits of the amplifier, but their time constants are chosen so as to maintain the proper phase relation between the 60-cycle voltages appearing at the tube electrodes.

The signal frequency of the multivibrator and flip-flop circuits is also sufficiently above that of the power supply to permit ordinary coupling capacitors to separate the two.

Voltage Regulation

The cycle of operation should start a little before the supply voltage wave has reached its peak in order that the duty cycle shall occur during the time when the supply voltage is changing by the smallest amount. This is also the time at which the vacuum tubes have their maximum transconductance and can give the largest nondistorted voltage output.

Since tube V_8 starts the cycle of events, it should fire just before the supply voltage wave has reached its peak. This tube has a linear grid-control characteristic, and if sine waves 180-degrees out of

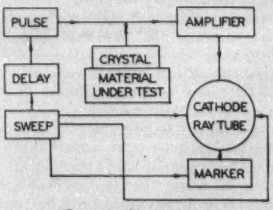


Figure 3. Block diagram

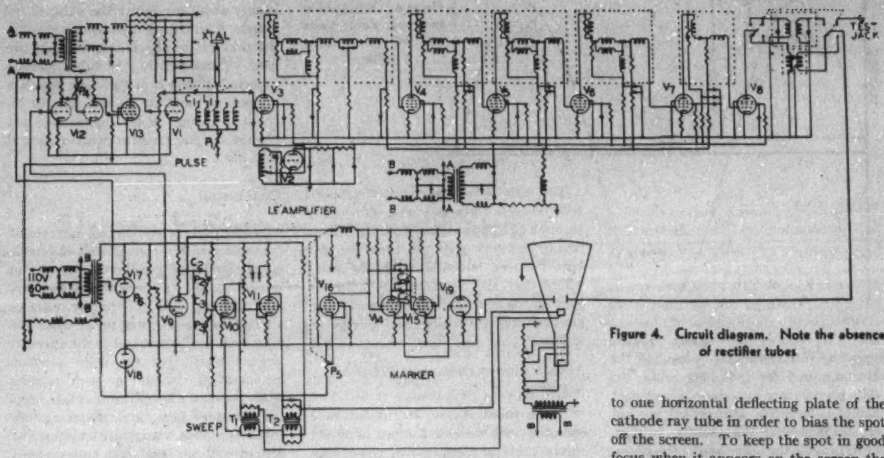


Figure 4. Circuit diagram. Note the absence of rectifier tubes

phase were placed on its plate and grid, the tube would either not fire at all, or would fire shortly after the plate voltage became positive. The difficulty might be overcome by changing the phase of the grid voltage with respect to the plate voltage, but then the tube would become what might be termed a negative voltage regulator. This may be explained by considering what would happen if the supply voltage should be increased. The effect on the firing point of an increase in plate voltage would be negligible compared to that of the increase in grid voltage since this type of tube has a control ratio of ten to one. The grid voltage, however, is negative, and an increase in it makes the tube fire at a later point in the cycle. Therefore, unless the increase in grid voltage were so large as to prevent the tube from firing before the positive peak in plate voltage, the other tubes in the reflectoscope would be operating at not only an increased voltage caused by the transient itself, but an additional increase because of the delayed firing of the sweep thyratron. Such a condition would magnify the effect of transients.

Not only may this magnification of the effect of transients be prevented, but the normal effect of transients may be reduced by the following means. Gaseous voltage regulator tubes V_{17} and V_{18} are used to clip the voltages applied to the plate and grid bias circuits of tube V_9. The negative grid bias derived from tube V_{18} is more than sufficient to prevent the thyratron from firing. This negative voltage and the plate voltage of tube V_9 are substantially constant during the duty cycle of the instrument because of the clipping by the voltage regulator tubes. The tube V_9 is then made to conduct by applying a sine wave of the supply frequency to the grid which is in phase with the plate voltage. The point in the cycle at which the tube fires may be moved back and forth by adjusting potentiometer P_6. Since the grid signal causing the tube to fire is positive when the plate voltages of the other tubes are positive, a transient causing an increase in voltage makes the thyratron fire earlier in the cycle, which tends to correct for the increase. But this correction is not complete as severe transients cause some movement of the sweep, probably because of the following reasons: the voltage regulator tubes allow some changes in voltage, some of the power transformers do not have linear output versus input characteristics, and the decoupling networks alter the shape of the transient before it reaches the tubes. This jitter is too small to be serious in normal industrial use.

The voltage across the positive voltage regulator tube is also used as plate voltage for the "flip-flop" and multivibrator circuits to increase the stability of these circuits.

Cathode Ray Tube

The fundamental frequency of the output of the sweep amplifier tubes V_{10} and V_{11} is quite near that of the supply voltage. Part of the positive plate voltage of the first tube may be applied directly to one horizontal deflecting plate of the cathode ray tube in order to bias the spot off the screen. To keep the spot in good focus when it appears on the screen the point inside the tube midway between the deflecting plates must be at ground potential. Therefore, it is necessary to use a negative bias voltage on the other horizontal deflecting plate equal in magnitude to the positive voltage applied to the first plate. Transformers T_1 and T_2 are used to introduce voltages which are in phase opposition to the operating plate voltages of the two tubes, it being impractical to separate the sweep output and operating plate voltages by means of capacitors. The transformer for the first

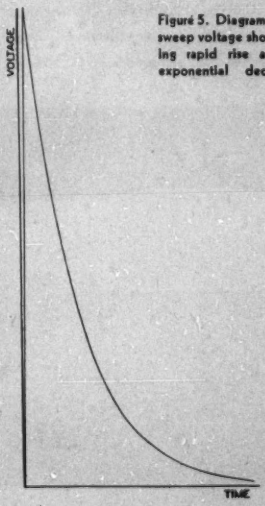


Figure 5. Diagram of sweep voltage showing rapid rise and exponential decay

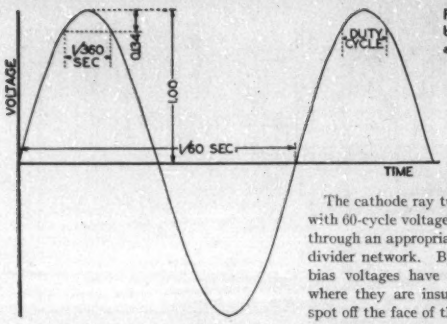


Figure 6. Relationship between duty cycle and 60-cycle supply voltage

tube V_{10} reduces the supply voltage appearing with the signal to one-half the value required for spot bias, while the other transformer introduces a sufficient amount of negative 60-cycle voltage not only to neutralize the positive voltage, but to provide the other half of the necessary negative bias. The transformer secondaries are shunted by resistors and capacitors to provide a low impedance path for the sweep voltage.

The cathode ray tube is itself supplied with 60-cycle voltages from a transformer through an appropriate resistance voltage divider network. By the time the spot bias voltages have fallen to the point where they are insufficient to keep the spot off the face of the tube, the electron gun voltages are so low that no spot is visible.

Advantages of Operating With A-C Power

If the tubes in the instrument were operated with several sources of direct current, several rectifiers and power supply filter networks would be required to supply the various voltages needed. The root-mean-square plate current of the tubes when operating from an a-c supply is only about one-fourth the value of the current if unidirectional voltages equal to the peak of the alternating voltages are used. Consequently, the tubes may be expected to have a much longer life than normal, or the plate current may be increased, giving more application and voltage output together with a normal tube life.

Conclusion

It has been shown that an instrument containing rather complicated electronic circuits and having a short duty cycle in relation to the idle period may be operated entirely with alternating voltages applied to the electron tubes when the duty cycle is synchronized to the alternatting voltage; that the main problem in operating circuits in such manner is to prevent the operating plate voltage of one tube from reaching the grid of the next without attenuating the desired voltage; and that this problem has been solved in most of the circuits in this reflectoscope by the use of the appropriate size of coupling capacitors between stages.

No Discussion

Electric Equipment for Cold Strip Reduction Mills

W. E. MILLER
MEMBER AIEE

THE COLD ROLLING of steel strip is a process that has been actively practiced now for over a half century. Fifteen years ago, the three fastest cold mills had delivery speeds of 800 feet per minute. Two of these high-speed mills of 1933 were single-stand reversing mills, and the other, a narrow width tandem mill.

Changes in mill equipment and electric equipment since 1933 have been noteworthy. Present-day reversing cold strip mills operate with delivery speeds up to approximately 2,000 feet per minute. Tandem cold strip mills recently placed in operation in the Pittsburgh area have been operated at delivery speeds in excess of 5,000 feet per minute. One of the mills which has electric equipment of the type to be described in this paper has been operated successfully at speeds in excess of 6,000 feet per minute. The engineering problems encountered in the design of the electric control system for a modern high-speed tandem mill are as interesting and as intricate as those encountered in any field of engineering. Not only must the system be designed for exacting steady state operating conditions, but it must have a high speed of response for operation under transient conditions.

Types of Mills

However, before it can be shown how the engineering problems have been solved, it is necessary to have an understanding of how steel is rolled, and the problems which arise in obtaining a uniform high-quality product. Most modern cold strip reduction mills are either four-high reversing mills or four-high tandem mills. There are, of course, other types of mills, but they have rather special characteristics and will not be considered here. The reversing mill and the tandem mill each has its own respective field of application, with some degree of overlapping between fields.

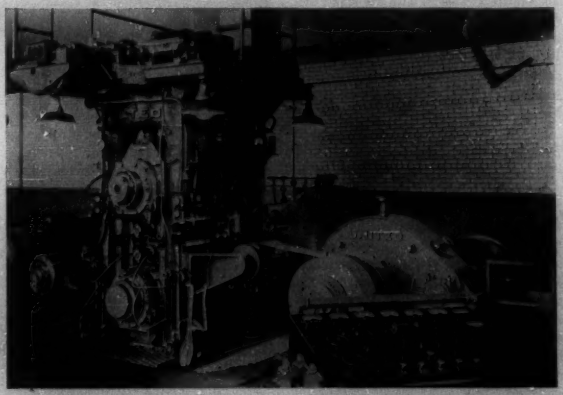
In the modern arrangement of these mills, both receive metal strip of uniform width and thickness in coil form reduce the thickness or gauge a certain amount, impart certain physical qualities to the product, and deliver the product in coil form.

The reversing mill, Figure 1, consists of a single-roll stand, and a tension reel on each side of the roll stand. Both the mill rolls and the reels are motor-driven and arranged for reversible operation. To effect the required reduction, the strip is passed back and forth through the single set of rolls for the necessary number of passes, the screws being reset between passes.

The tandem or continuous mill, Figure 2, consists of from three to five roll stands and a tension reel arranged in a continuous line, so that the strip passes successively through the several roll stands, and is coiled under tension as it is delivered from the last stand.

As to field of application, the reversing mill has been considered adaptable to a relatively moderate production of several types of products, while the tandem mill, in general, has been considered to be a high production, single purpose unit. Thus it is, that tandem mills are usually designated as tin plate mills or sheet mills. However, a tin plate mill is generally laid out so that sheet can be rolled, and a sheet mill made capable of rolling tin plate with two passes through the mill. Such great changes in rolling schedules are made with a reduction in efficiency in

Figure 1. Typical reversing cold strip mill



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W. E. Miller is with the steel mill division, industrial engineering divisions, General Electric Company, Schenectady, N. Y.

Table I. Power Calculations

Rolling 32 Inches Wide by 0.095 Inch Mild Strip to 32 Inches Wide by 0.0091 Inch Thick
5 Passes—4,500 Feet Per Minute

1.	Pass number	1		2		3		4		5		Reel	Total
2.	Strip thickness	0.095	0.057		0.034		0.020		0.0125		0.0091		
3.	Strip cross section	3.04	1.82		1.09		0.64		0.40		0.291		
4.	Per cent reduction	40		40.5		41		37.5		27			90.4
5.	Total elongation	1.00	1.67		2.80		4.75		7.60		10.4		
6.	Strip fpm	430	720		1,210		2,045		3,295		4,500		
7.	Net tons per hour	133.5		133.5		133.5		133.5		133.5			133.5
8.	HP hr per ton												
	(a) total (from curve)		18.5		43		71		99		119		
	(b) per pass	18.5		24.5		28		28		20			119
9.	Net rolling hp	2470		3300		3740		3740		2670			15,920
10.	Strip tension												
	(a) Per sq. inch		18,000		20,000		28,000		40,000		8,000		
	(b) Total		32,800		21,800		17,900		16,000		2,330		
11.	Tension hp		720		800		1,110		1,600		320		
12.	Net motor hp	1,750		3,220		3,430		3,250		3,950		320	15,920

$$\text{Net tons per hour} = \frac{(\text{Area in sq. inches}) \times \text{fpm}}{9.82}$$

$$\text{Net rolling hp} = \text{Net tons per hour} \times \text{hp hr per ton per pass}$$

$$\text{Tension hp} = \frac{\text{Total tension} \times \text{strip fpm}}{33{,}000}$$

the use of the mill. It has been said that the reversing mill stresses flexibility, and the tandem mill production.

Both types of mills are driven by d-c motors supplied from adjustable voltage motor-generator sets. A few years ago, it was general practice on both types of mills to supply all d-c motors from a common bus, with one or more d-c generators supplying power to the bus. Speed adjustments between stands or on the reels were made by motor field control. It has been recent practice on reversing mills to supply power to the stand motor and reel motors from separate generators and not from a common bus. On most high-speed tandem mills now in service, all the d-c motors are supplied from a common bus. Adjustment of individual stand and reel speeds is by motor field control, and on some mills installed in recent years by so-called *IR* compensation line boosters in series with the stand and reel motor armature circuits. Such a scheme for a tandem mill is shown in Figure 3 An explanation of *IR* compensation and of a newer scheme including *IR* compensation, but without the series line boosters, will be given later.

Problems in Rolling Cold Strip

In tandem cold strip mills of the modern high speed type, the hot rolled strip coil is threaded into the entering end of the cold mill with the finishing end operating at speeds of 100 to 600 feet per minute. During or after the threading of the delivery tension reel, the mill may be accelerated to a top speed of from 4,000 to 5,000 feet per minute, or on mills now going into service even higher. After rolling most of the coil at top speed, the mill is decelerated to threading speed as the tail end of the strip is approached. It is the desire of the operator to have the strip "on gauge" at all times, or, failing in this, to get on gauge as soon as possible as the mill is accelerated and to stay on gauge as long as possible as the mill is decelerated.

There are several factors involving the working of the metal during rolling, and the balancing of stand motor constants which materially affect the strip gauge during periods of acceleration and deceleration. Tests have been made which demonstrate conclusively, even on a single stand mill, that the gauge will be heavier at low speeds than at high speeds, and heavier with low tensions than with high tensions, if all other factors remain constant. Just why the gauge goes heavy at low speeds is not completely understood, but electric means of control can be used to compensate for the effect in spite of the obscure cause.

If moments of inertia (motor plus mill) are properly balanced between stands, each motor will require the same per cent current for acceleration. If, in addition, the per cent rolling load on all stands is the same and if the per cent rated speed regulation and the per cent operating speed of all motors is the same, then as the mill is accelerated and decelerated, all motors will remain in speed correspondence. If a variety of products is to be rolled on a mill, the flexibility of adjustment required between stands by motor field control alone is such that all the conditions given are not satisfied. Practical limitations in motor and mill design also limit the extent to which it is possible to balance the moments of inertia.

It will generally be found that the per cent accelerating current required for a given set of conditions will increase from the first to the last stand. Thus, during periods of acceleration and deceleration, there will be a loss of speed correspondence between stands. For example,

Figure 2. Typical 5-stand tandem cold tin plate mill

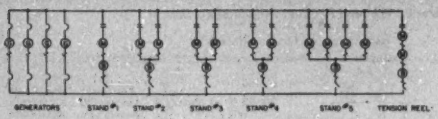


Figure 3. Armature circuit connections for a 5-stand tandem tin plate mill with generators connected to a common bus and using series boosters for *IR* compensation

Dynamic braking resistors and contactors not shown. Total motor capacity 16,850 horsepower

during acceleration the last stand will tend to accelerate slower than the stand preceding it, and it can be seen that there will be a decrease in tension. When all motors are up to speed, tensions will be normal. During deceleration, the last stand will tend to slow down at a lower rate than the stand preceding it, causing an increase in tension between stands. Thus, due to normal mill inertia effects alone, the mill will tend to go from "heavy" to "on gauge" as the mill is accelerated and from "on gauge" to "light" as the mill is decelerated.

The combination of the low-speed effect and the effect of mill inertia on tension helps to explain some of the many tendencies, making it harder to get on gauge during acceleration than to keep on gauge during deceleration. As the operating speeds of mills are increased, the tonnage of material rolled during the accelerating and decelerating periods increases, and the importance of getting the mill on gauge during the early part of the accelerating period becomes more important. On new high speed tandem mills coils weighing 30,000 pounds are being used. If such a coil is 32 inches, in width and is finished to 0.0091 inch, its length will be 30,700 feet. If it takes six seconds to accelerate the mill from a threading speed of 500 feet per minute to a running speed of 5,000 feet per minute, a total of 275 feet of strip will be delivered from the mill. The same amount of strip will also be delivered during deceleration to threading speed. Reduction of the percentage of this material that is off gauge during acceleration and deceleration can be obtained by electric means through proper selection and design of mill motors and control.

Selection of Electric Equipment

Before the motors and control for a cold strip mill can be selected, certain mill data must be obtained and studied. First, it is necessary to know the type of material to be rolled and the tonnage required. Since the maximum reduction per stand is limited, the type of product more or less determines the number of mill stands. Sheet mills usually consist of three or four stands while most tin plate mills consist of five stands. Rolling schedules for the various products must be worked out and rolling speeds obtained. The selection of the motors depends directly upon the rolling schedules for which the mill is designed.

The method most commonly used to calculate the loads which will be imposed upon the driving motors of a proposed mill, when rolling a particular product at a specified rolling speed with a specified reduction in each stand, makes use of test data obtained from the operation of other mills of the same type. To make the test data generally applicable, it is reduced to the basis of energy units per unit of rolling work, and plotted against reduction in curve form. A typical curve for mild steel such as forms the great bulk of strip tonnage is shown in Figure 4. Use of the curve is demonstrated in Table I where a set of calculations for a rolling schedule is given.

Another method sometimes used in the calculation of rolling power requirements is to figure the rolling pressure by one of the methods using basic resistance to compression, and from the pressure, and the lever arm and speed, calculate the rolling power. However, when rolling power curves are available, their use is generally preferred since they are based on accurate electrical data.

A typical set of power calculations for

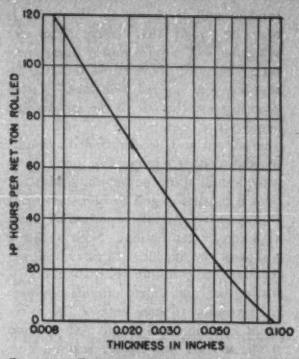


Figure 4. Typical power curve for cold rolling strip steel on a 5-stand tandem mill from 0.095 to 0.0091 inch thick. Mild open hearth steel

one schedule on a five-stand tandem tin plate mill is shown in Table I. A set of such calculations must be made for every schedule. Motor horsepower selection should be based on the heavier schedules. Strip speed through the mill and strip width do not seem to affect power consumption per ton of product in the usual operating range.

Motor Selection

A study of the set of rolling schedule calculations for the mill will permit the selection of motor horsepower ratings and speed range requirements for each stand. It is usual practice to plot the stand speeds for each rolling schedule. The maximum and minimum speeds required for each stand determine the speed range required of that stand motor. A plot of maximum and minimum speeds for each mill stand gives the so-called "speed cone." A typical speed cone for a five-stand

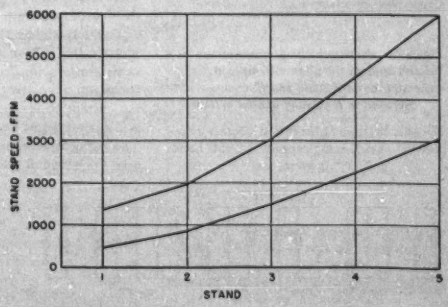


Figure 5. Typical speed cone for a 5-stand tandem tin plate mill

tandem mill is shown in Figure 5. It may be found that some infrequent schedules will require motor horsepower ratings slightly above those required by the heaviest normal schedule. Rather than increase the size of the motors and penalize the operation of the mill during acceleration and deceleration on normal schedules, it is present-day practice to provide mill stand power units with full class *B* insulation and to rate them on a 40 degrees centigrade continuous basis with forced ventilation. Such a rating will allow the machines to carry a 20 per cent overload continuously without injurious heating, and should provide the margin necessary to take care of the infrequent schedules and permit rolling some of the normal and lighter schedules at higher speeds at some future date.

The selection of the motor horsepower rating and speed range for each stand does not completely specify that motor. Actual motor speed, peak rating, and type of drive also must be given. These quantities should be selected on the basis of keeping the stored energies as low as possible and maintaining as near a balance as possible between stands. The peak rating of the motor is dependent upon the total Wk^2, since the mill must be accelerated carrying full rolling load plus the power required to accelerate the motor and the mill.

Since it is the effect of the Wk^2 on the electrical system which is of concern, Wk^2 for each stand must be obtained referred to the stand motor shaft. The Wk^2 of the stand motor shaft will be affected by three factors: motor Wk^2, gear ratio, and mill Wk^2. There are various drive arrangements which may be used. The simplest form consists of a single armature motor driving the mill through a pinion stand of 1:1 ratio. Double armature motors may also be used with this arrangement. In addition to reduction of Wk^2, additional advantages are obtained through the use of so-called twin drive motors where each roll is driven by a separate motor. Here, again, it may be necessary to use double armature motors, thus having a double armature twin drive motor consisting of four separate armatures.

Since the speed of the material increases as it goes through the various stands, it is necessary to either use higher speed motors at the finishing end or to use speed increasing gears of increasing gear ratio on the finishing stands. On low speed mills, speed decreasing gears may be used on the entering end stands. When referred to the motor shaft through speed increasing gears, the mill Wk^2 will be increased by the square of the gear ratio. Thus, although the mill external Wk^2 may be the same on every stand, the effective mill Wk^2 (referred to motor shaft) increases from the entering stand to the finishing stand. If any attempt is to be made to balance the kinetic energies, it thus becomes necessary to reduce the motor Wk^2's on the finishing end of the mill by increasing the number of armatures used. Thus, it will be found on large mills that the first stand may consist of a single armature motor driving through a 1-1 pinion stand, while the fifth stand may consist of a double armature twin drive motor with four armatures driving through speed increasing gears.

Table II shows the Wk^2 of the stand motors, the Wk^2 of the mill, and the per cent accelerating current to accelerate the motor only, and the per cent accelerating current to accelerate both motor and mill for a typical five-stand tandem mill. The per cent accelerating current, for the sake of convenience, is based upon accelerating from zero to maximum speed in ten seconds.

Table II. Wk^2 and Per Cent Accelerating Current

Five-Stand Tandem Tin Plate Mill

Stand	Total Hp	No. of Arm's.	Type of Drive	Motor Rpm	Gear Ratio	Wk^2-lb ft² at Motor Shaft × 1000 Motor	External	Total	Per Cent Amperes to Accelerate Mill and Motors*	Per Cent Amperes to Accelerate Motors Only*
1	1750	1	SA	80/240	1:1	123	38.5	161.5	33.0	23.0
2	3500	2	DA	150/360	1:1	100	38.5	138.5	31.8	23.0
3	3500	2	SA-T	175/350	1:1.525	74	96.2	170.2	36.9	15.6
4	3500	2	SA-T	200/400	1:2	66	192.0	258.0	73.0	18.7
5	4000	4	DA-T	275/535	1:2	36	192.0	228.0	101.0	15.9
Reel	600	2	DA	300/1200	1:1	3.1	3.5	6.7	99.5	46.0

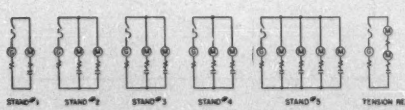
SA—Single armature.
DA—Double armature.
SA-T—Single armature, twin drive.
DA-T—Double armature, twin drive.
*Acceleration in 10 seconds from rest to top rated speed with weak field setting on each motor.

While it has been said that the per cent accelerating current should be equal on each stand, it is evident from Table II that this has not been accomplished. The Wk^2 and the kinetic energy could be increased artificially on the stands at the entering end of the mill, but such a procedure would certainly be undesirable. A study of Table II indicates that it is impossible to materially reduce the kinetic energies of the last stands by changing the motors because the greater part of this energy is in the mill itself. For instance, to achieve any reduction on stand number 5, it would probably be necessary to go to eight armatures, each rated 500 horsepower. Such an arrangement would not only make the motors but also the control cost considerably more. Thus, it will usually be found on large mills that the per cent current to accelerate the mill and motors increases from the entering to the finishing end of the mill.

Where cold strip mills operate at very high delivery speeds, it becomes very important to keep the peripheral speed of the upper and lower working rolls the same at all times. On the finishing end of a mill rolling thin gauges, there is considerable pressure between rolls, even with no metal in the mill. With any difference in working roll diameters on a pinion driven mill, this would make the rolls grind each other and cause their surfaces to become rough. This is avoided with separate roll drives or twin drives which permit the rolls to be operated at slightly different angular speeds.

Figure 6. Armature circuit connections for a 5-stand tandem tin plate mill with individual generator for each stand and *IR* compensation included in regulating control system

Dynamic braking resistors and contactors not shown. Total motor capacity 16,850 horsepower



Generator Selection

Selection of the generators to supply d-c power to the mill motors is dependent

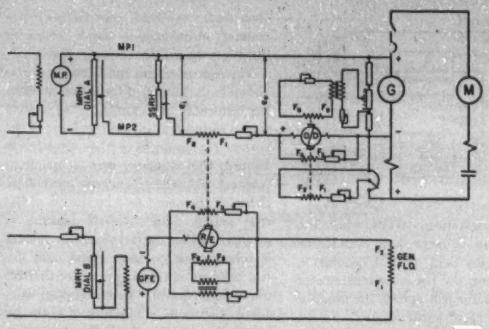


Figure 7. Amplidyne type voltage regulating control circuit showing ohmic drop compensation and stabilizing circuits for one stand of a 5-stand tin plate mill using an individual generator for each stand

upon both the mill motor horsepower ratings and the control scheme to be used. As the stage of the art of cold rolling has advanced, the higher and higher mill delivery speeds have raised the power requirements to the point where it is necessary to use almost as many generators on the bus as there are mill stands. It is evident that the flexibility of the tandem mill can be increased if the number of generators is equal to the number of mill stands, and the main bus is broken so that a separate generator then supplies the motor driving each stand. Adjustments between mill stands can then be made by generator voltage control as well as by motor field control. Thus, the speed range of each stand is widened at the lower end by reducing the horsepower output. In addition, the *IR* compensation function can be included in each stand generator regulating circuit, and the bothersome series boosters can be eliminated.

It has been found where large amounts of power are required that the most economical generator rating is about 4,000 kw. For the 5-stand tandem mill being considered, it can be seen from Table II, that a total of 13,000 kw of generating capacity necessitating at least four generators is required. In addition, a fair size booster generator would be required for the reel circuit. Since four large generators and one large reel booster will be required on the common bus scheme, the advantages are definitely in favor of the individual generator scheme requiring only one additional generator, but permitting the omission of all the high current *IR* compensation boosters, the purpose of which will be explained later. The generators selected are given in Table III, and are arranged into two motor generator sets.

Since it is necessary that the generators be able to handle any loads which the motors will carry, even when taking into account the 20 per cent overload capacity sometimes incorporated in the motors, the generators must also be completely insulated with class *B* materials. Both motors and generators are rated 600–750 volts direct current in order to have line currents at the lowest possible level without getting into difficulties inherent with higher voltages.

Selection of Control

The majority of the up-to-date tandem mills now in service are equipped with separate d-c motors on each stand, supplied as a group with d-c power from an individual generator, or a group of generators connected to a common bus and operating as a unit. The majority of the high-speed high-power tandem mills now going into service differ in that an individual generator supplies power to each stand motor. Armature circuit connections for the individual generator scheme are shown in Figure 6.

The choice of the basic control scheme is a matter of economics. Certainly, on a high-speed high-power mill the advantages of the individual generator scheme are many. First, the high-current series booster generators and their control are eliminated. Second, the heavily supported high-current bus is eliminated. Third, and most important, is that each generator may, if desired, be operated at a different voltage.

On a high-speed tandem mill electric gauge control during the mill accelerating and decelerating periods is a "must." Gauge control can be obtained during these periods if the mill motor speeds are changed in such a way as to automatically adjust the strip tension between stands. There are two general methods of control. The first is by automatic control of the shunt field strength of one or more of the stand motors, the method being called "tapered tension control by motor field adjustment." The second is by an independent control of motor armature voltage by an amount equal to or a part of the *IR* drop in the system, the method being called "*IR* drop compensation." In the past, where *IR* compensation has been provided, it has been general practice to use the series booster generators for this purpose. On new mills with the individual generator scheme of control, the series booster generators are eliminated and the *IR* compensation function is included in the regulating control system.

While both tapered tension control and *IR* compensation make it possible to get on gauge sooner as the mill is accelerated and to stay on gauge longer as the mill is decelerated, the "*IR* drop compen-

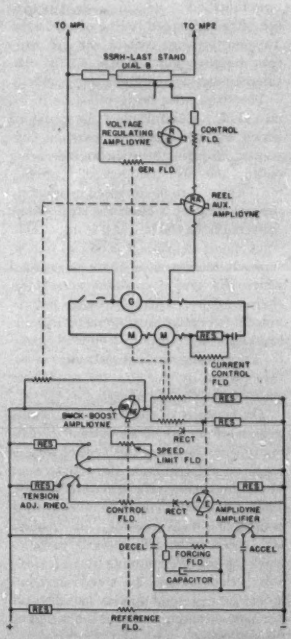


Figure 8. Amplidyne type regulating control circuit for tension reel on a tandem cold strip mill showing tension (current type) regulator and voltage regulator following reference set up by last mill stand

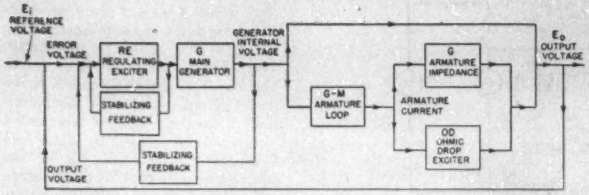


Figure 9. Block diagram for amplidyne type voltage regulator with *IR* compensation for one stand of a 5-stand tandem cold strip mill using individual generator for each stand. Schematic control circuit shown in Figure 7

sation" method is more nearly automatic than is the "tapered tension" method. With *IR* drop compensation, the motors more nearly "sense" gauge and automatically taper the tensions as needed. With tapered tension control, it is necessary that the tapered tension rheostats be set in advance. Thus it is that *IR* drop compensation seems to be the preferred method of electric gauge control in the ultra-high speed rolling of cold strip. Tapered tension control, or *IR* drop compensation, may or may not be considered necessary on lower speed mills.

The strip, after being rolled, is reeled into coils. Control must be provided which will automatically maintain constant tension in the strip as it is being reeled, and also automatically provide for the change in motor speed required by the reel build up. Since the strip will be delivered from the last mill stand at constant speed, and since it is desired to hold constant tension, a regulator is required which will give a constant horsepower characteristic. Such control can be provided by a regulating tensiometer measuring tension directly or by a current regulator maintaining constant reel motor armature current by changing the shunt field excitation of the reel motor.

The majority of the tension regulators now in service are of the current regulating type. The use of this type of tension regulator is based upon the maintenance of constant reel motor armature voltage. Thus, reel build-up must be taken care of entirely by field control. On modern high speed mills, coils must be large to make the full speed running time a reasonable period. Thus, the speed range required for reel build-up may be large and becomes a limiting factor in the design of the reel motor.

With the tensiometer type of tension regulator, automatic adjustments to hold constant tension may be made by changing both motor armature voltage and motor field excitation. Thus, where the coil build up is large and permissible speed range by field control becomes a limiting factor, the use of a tensiometer type of regulator will permit the use of a more nearly normal reel motor.

Control Circuits

The control circuit for the individual generator scheme for a single stand is shown in Figure 7. It is seen that the mill master rheostat determines the voltage on the master voltage control bus *MP*1 and *MP*2. This bus is the standard by which the voltage on every generator is controlled. By raising or lowering the voltage on this bus, it is possible to raise or lower (proportionately) the voltages of all the generators. The mill master rheostat also controls the excitation on the generator field exciter (*GFE*) which supplies all stand generators. In this way, the regulating exciters (*RE*) on each stand may operate at low voltage levels ideal for fast regulator action.

Individual voltage adjustment on each generator may be accomplished by movement of the stand master rheostat. Each stand motor speed can also be controlled by adjustment of its shunt field current. However, regardless of the settings on the stand master rheostats, the mill master rheostat will raise or lower each generator voltage proportionately.

It has already been pointed out that it is usually desirable not only to control all generator voltages so that they are proportional to the voltage on the master voltage control bus, but also to compensate for *IR* drop in all motor armature circuits. In this way, the speeds of all motors may be held closely proportional to the voltage on the master voltage bus. It can be seen in Figure 7 that the ohmic drop exciter (*OD*) receives its excitation from the voltage drop across the generator commutating field, and its output voltage may be made equal to or any fixed percentage of the motor armature circuit *IR* drop. The voltage of this amplidyne is automatically subtracted from the generator terminal voltage. Each stand regulator, through its regulating exciter (*RE*) then acts to hold essentially motor counter electromotive force which is directly proportional to motor speed.

The control circuit for a tension reel is shown in its simplified form (less stabilizing elements) in Figure 8. The system shown includes a tension regulator of the current regulating type. As already explained, this regulator acts to maintain constant reel motor armature current by changing the shunt field excitation of the reel motor. The over-all control is arranged so that the reel generator voltage is proportional to strip speed, and the reel build-up is taken care of by the reel motor field control. Reel generator voltage is, therefore, a function of both the last stand generator voltage and motor field weakening.

Selection of Circuits and Circuit Constants for Optimum Performance

In no other type of rolling mill does the performance of the electric equipment affect the quality of the product to a greater extent than in the case of the cold strip mill. The accuracy of the gauge, the ease of maneuvering, the mill tonnage, and the per cent yield all depend directly upon how well the electric drive system is co-ordinated. Not only must the system be designed to operate over an extremely wide range of steady state operating conditions, but it must be designed to have a high rate of response so that changes in operating conditions may be made quickly with all stands acting together.

Regulating circuits of the type shown in Figures 7 and 8 are also called servomechanisms. The study of such circuits has been both intensive and extensive during the past few years. Actual circuits have been tested to study transient performance in the laboratory, factory, and field. System studies have been made on differential and transient analyzers. Complete servomechanisms have also been analyzed by the frequency response methods of Nyquist and Bode.

Table III. Main Generator Ratings

Five-Stand Tandem Tin Plate Mill

Main Generator Set Number	Stand Number	Generator kw	Generator Voltage
1	1	1,500	750
1	2	3,000	750
1	3	3,000	750
2	4	3,000	750
2	5	3,200	750
2	Reel	500	750

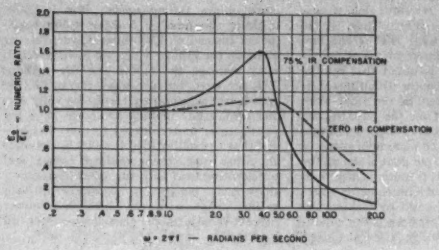


Figure 10. Frequency response of voltage regulator with *IR* compensation for stand number 5 of tandem tin plate mill with motor operating with weak field

By correlating the data available, it is possible to analyze the systems of Figures 7 and 8 by the frequency response method, and to select the circuits and circuit constants which will give the desired system performance.

The complete amplidyne-type regulating control circuit for one stand of a five-stand tandem tin plate mill is shown in Figure 7. The control for every other stand is identical. Two small size identical transformers are used to obtain voltage rate of change signals for stabilization. The ohmic drop exciter (*OD*) is also used as an amplifier for the voltage rate of change signal fed back from the main generator (*G*). The amplidyne exciter used for this application is ideally suited for its dual purpose because of its high gain and very low time constant. The time constant of amplidyne *OD*, as shown in Figure 7, is less than 1/300 of the time constant of the main generator.

The block diagram for the regulating system of Figure 7 is shown in Figure 9. In constructing the diagram, it was assumed that the error introduced by considering the generator voltage feedback signal to be proportional to the rate of change of internal voltage instead of terminal voltage is small.

The basic method of analysis and the mathematical justification of the frequency response method of analysis have been completely presented in available published literature. Briefly, it is necessary to apply an alternating input signal of constant magnitude and varying frequency to the input side of the system and to determine both the magnitude of the output signal and its phase relation with the input signal. The frequency response must be determined over the whole frequency spectrum of the system. To do this mathematically, it is usual practice to break the system up into its component parts as shown in Figure 9, and to determine the transfer function of each part. These transfer functions are then combined either mathematically or graphically to obtain the system transfer function and the system frequency response. From a study of the frequency response and phase characteristics of the system components and the whole system, it is possible to select and adjust circuits so as to cause the system to have the desired transient characteristics.

Frequency response curves for the system of Figure 7 are shown in Figure 10. It is seen that the effect of *IR* compensation is to increase the peak response of the system. However, with 75 per cent compensation the peak is well within the limits recognized as required for good performance. Tests have been made and reported upon before the Association of Iron and Steel Engineers upon the advantages and disadvantages of *IR* compensation. A study of the results of these tests indicates that 75 per cent *IR* compensation is about the maximum amount which can be used to advantage on a system made up of present day mill type motors and generators.

Recent Tandem Cold Strip Mill Installations

There are five tandem cold strip mills now in service or soon to go into service which have electric equipment and individual generator control systems of the type described in this paper. Control systems on all five mills were designed using the frequency response method of determining transient performance. Initial results have been gratifying in that the regulating systems have performed as predicted. The first mill was placed in service in the Pittsburgh area in October 1947, and it is expected that the fifth mill will be rolling steel in the Chicago area during the early summer.

Mill operating and maintenance men are well satisfied with the individual generator scheme of control for tandem mills. Increased flexibility in setting up rolling schedules through adjustment of stand generator voltage, elimination of the high current series *IR* compensation boosters and reduction of the current capacity of d-c buses are factors of such importance on the modern high-speed high-power tandem mills that it is felt that all future installations will be of this type. Amplidyne type regulators designed for fast transient performance, in accordance with the latest circuit theory and extensive tests, promises to be the tool which will hold the stands together to form a finely co-ordinated mill capable of rolling a high quality steel product.

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Discussion

A. F. Kenyon (Westinghouse Electric Corporation, East Pittsburgh, Pa.): In Table II of the paper, the Wk^2 values are given as referred to the motor shaft. Studies of the drives for a proposed mill often include comparisons of direct-connected motors running at the mill speed with geared motors with either a step-up or step-down ratio between the motor and the mill, and we have found that referring all Wk^2 values to the mill working rolls affords a more direct comparison of the several alternate drive arrangements.

We have compared the Wk^2 values given in Table II with the corresponding inertias of a similar 42-inch five-stand tandem tinplate mill put into operation in May 1947, this mill having 17,550 horsepower total capacity of mill and reel motors, and 5,000 feet per minute maximum rolling speed. By coincidence, the total Wk^2 of the number 4 and 5 stands of the two mills are almost identical. In the mill described by Miller, the number 5 stand is driven by a

4,000 horsepower twin-motor in which each 2,000 horsepower motor is a double armature unit. The double armature construction achieves minimum motor Wk^2, however, the two motors are arranged side by side, requiring relatively large center-to-center distance and hence relatively large Wk^2 of the step-up gearing. In the mill of which we speak, the number 5 stand is driven by a 4,500 horsepower twin-motor in which each 2,250 horsepower motor is a single armature machine. The simpler single armature construction of course results in larger motor Wk^2; however, the two motors are axially displaced so that the shaft of one motor extends over the frame of the other motor, with resultant smaller gear center distance and Wk^2. Thus, it so worked out that the total Wk^2 of the two arrangements were almost identical.

From Table II, it is noted that on the number 5 stand, the motor armatures constitute only 16 per cent of the total Wk^2. Also, it is noted that on the basis of the 4,000 horsepower motor rating, the torque required to accelerate to maximum speed in ten seconds is over 100 per cent of the motor full load torque. This is much higher than on the other stands, and a much better overall balance can be achieved by making the number 5 motor of larger horsepower capacity than is indicated by the calculated steady-state rolling loads. For instance, making the number 5 drive 4,500 horsepower instead of 4,000 horsepower, (as was done on the mill to which we have referred) may increase the motor Wk^2 by perhaps $7^1/_2$ per cent, but increases the total Wk^2 and hence accelerating torque by only $1^1/_2$ per cent, while making available $12^1/_2$ per cent greater motor output torque. Assuming that the rolling load is 4,000 horsepower in either case, the combined rolling and accelerating load with the 4,000 horsepower drive is 201 per cent of the motor full load rating, while with the 4,500 horsepower drive it is only 180 per cent of the motor full load rating.

A Study of A-C Arc Behavior Near Current Zero by Means of Mathematical Models

T. E. BROWNE, JR.
MEMBER AIEE

Synopsis: The differential equations of two idealized models of the arc column proposed by previous writers have been combined into a composite model whose behavior approximates that of actual a-c power arcs in circuit breakers and fuses near a critical current zero for arc extinction. An extensive series of curves are presented showing, for this model, the variation with time just before and after current zero of arc resistance, arc current, arc power, and dielectric strength of the arc space under certain simplifying assumptions for three different forms of circuit recovery voltage. These quantities are expressed in terms of prior arc current and voltage and of a characteristic "time constant" which depends upon the physical conditions imposed upon the arc. The model curves are compared with oscillographic measurements. The usefulness of such models in aiding the understanding of arc behavior in circuit interrupters and in correlating and making extrapolations from interrupting data is pointed out.

IN SCIENCE and engineering, calculations are generally made on the basis of precise theoretical pictures, or "models," which are only approximations to the actual physical phenomena represented. As knowledge of phenomena grows, rough simple pictures give way to more faithful and usually more complex pictures. Until recently, there have been available no pictures of dynamic electric arcs of sufficient clarity and precision to permit their use as theoretical models for more than rudimentary calculations. A common and useful approximation, or over-simplification, in dealing with a-c arcs in circuit interrupters has been to ignore the arc's resistance up to, and its conductance just after, a critical current zero for arc extinction. This has led to the expression, "currentless pause" or "current-zero period" which, while pertinent to some low-current or low-voltage cases, may be seriously inaccurate and misleading when dealing with short-circuit arcs in high-power circuits. The necessity for considering the arc column as a rapidly changing but always finite conductor was pointed out some years ago by Slepian,[1,2] who portrayed the extinction or reignition of a conducting arc column graphically but did not then attempt to make any precise calculations. This concept of the finite arc interacting with its connected circuit has been used by Cassie[3] in deriving a simple criterion for "circuit severity" which yields striking results in correlating interrupting data obtained with air-blast circuit breakers. This correlation is confirmed by unpublished results obtained at the Westinghouse High Voltage Laboratory as well as by those in Cassie's paper. The practical possibilities, illustrated by Cassie's work, of such a mathematical attack upon the a-c arc extinction problem are so attractive that a preliminary and comparatively elementary exploration of the mathematical approach to dynamic arc behavior has seemed to the author to be desirable.

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T. E. BROWNE, JR., is research engineer with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

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Choice and Description of Arc Models to Be Employed

General

Space limitation prevents a complete treatment here of the whole problem of arc behavior in any circuit. In the most usual practical case, however, a circuit breaker arc serves most of the time as a relatively good conductor in a circuit whose impedance is high compared with the arc resistance (the arc voltage is low compared with the circuit voltage), and in which, therefore, the current is determined almost exclusively by the circuit. In the vicinity of a normal current zero, however, the arc resistance may become so high as to reverse the situation so that the circuit becomes the principal regulator not of the arc current but of the arc voltage. The calculations of this paper will be directed particularly toward this latter high-arc-resistance situation which may lead either to "arc extinction" (complete loss of arc conductance) or to "arc reignition" (return of relatively high arc conductance). The transition situation in which the arc current and voltage are determined jointly by the circuit and by the arc, though often of much importance and thus worthy of further treatment, will be considered in the present study to be of vanishingly small duration and coincident with the passage of the current through zero. This and other idealizations, made for the sake of simple mathematical treatment, still permit, however, the derivation by means of mathematical models of a number of interesting and useful relations describing the dynamic behavior in its circuit of an arc of measurable conductance.

To represent properly the known behavior of the dynamic arc, an arc model equation should be of the general form:

$$\frac{i}{e}=\frac{1}{R}=f(W, N, t), \qquad (1)$$

where

i = instantaneous arc current
e = instantaneous arc voltage gradient

R = instantaneous arc resistance per unit length $= e/i$

W = power input to the arc per unit length $= ei$

N = power loss from the arc per unit length

t = time

or in words, the arc's conductance must be a function of the arc power input and power loss and of time—the time being necessary to account for the lag in conductance which characterizes dynamic arc behavior. In arriving at working equations for the arc, recent investigators[3,4,5] have further assumed that

$$\frac{1}{R} = F(Q) = F\left[\int (W-N)dt\right] \tag{2}$$

where Q, the energy content per unit length of the arc associated with its elevated temperature and state of ionization, is, as indicated, a result of the arc's past history. A convenient general form for the differential equation of the model arc based upon this assumption may be obtained by differentiating equation 2 and then dividing by it to give

$$R\frac{d}{dt}\left(\frac{1}{R}\right) = \frac{F'(Q)}{F(Q)}\frac{dQ}{dt} = \frac{F'(Q)}{F(Q)}(W-N) \tag{3}$$

or, in terms of e and i,

$$\frac{1}{i}\frac{di}{dt} - \frac{1}{e}\frac{de}{dt} = \frac{F'(Q)}{F(Q)}(ei-N) \tag{4}$$

The specific form of this differential equation will depend only upon the particular forms selected for $F(Q)$ and N.

Two different equations of this type based on rather different concepts of the physical nature of the arc column have been used. Mayr,[4,5] following a rather involved derivation based upon the assumption of thermal ionization according to the *Saha* equation and of heat loss by thermal conduction alone, has arrived at the forms

$$\frac{1}{R} = F(Q) = K\epsilon\frac{Q}{Q_o} \tag{5}$$

and

$$N = N_o \text{ (a constant)} \tag{6}$$

This, of course, means that the "static characteristic" of his arc is

$$ei = N_o \tag{7}$$

a rectangular hyperbola, and that the dynamic behavior of the arc is governed by a characteristic energy quantity, Q_o. The loss or gain of this energy quantity, Q_o, changes the arc's conductance by the factor ϵ (2.718). Mayr pictures the arc as occupying a cylindrical space of constant radius within which the temperature varies with both the radial dimension and time. Substitution of equations 5

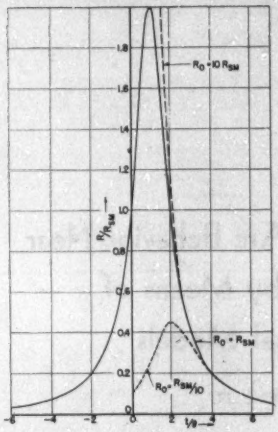


Figure 1. Arc resistance versus time from Mayr's dynamic arc equation for linear current change through zero ($I = I_1\omega t$)

R_o = actual arc resistance at current zero

R_{sm} = arc resistance at current zero for cyclic steady state (Mayr)

$$= \frac{E_p}{\Omega I_1(\omega\theta)^2}$$

E_p = arc voltage at current peak

$$\left(i = I_1 t, \frac{di}{dt} = 0\right)$$

θ = time constant of arc column

and 6 in equation 4 yields Mayr's differential equation,

$$\frac{1}{i}\frac{di}{dt} - \frac{1}{e}\frac{de}{dt} = \frac{ei-N_o}{Q_o} \tag{8}$$

Consideration of this equation in combination with that of the connected circuit, as done by Mayr, yields results which are of much interest but which, as stated, are beyond the scope of this paper.

Cassie,[3] apparently in view of the studies of an arc in an air blast by Kirschstein and Koppelmann,[6] assumes an arc column within which the temperature is fixed and uniform in space and time, having

1. Constant per-unit-volume resistivity, μ.
2. Constant per-unit-volume power loss, λ.
3. Constant per-unit-volume energy content, c.
4. A cross-section area, A, varying with current and time according to the constraints 1, 2 and 3, which require that in the steady state the current density and voltage gradient also be constant, giving a static characteristic,

$$e = \sqrt{\mu\lambda} = E_o, \tag{9}$$

independent of the current. These assumptions lead to

$$\frac{1}{R} = F(Q) = \frac{A}{\mu} = \frac{Q}{\mu c} \tag{10}$$

and

$$N = A\lambda = \frac{\lambda Q}{c} \tag{11}$$

which, when substituted in equation 4, give the differential equation

$$\frac{1}{i}\frac{di}{dt} - \frac{1}{e}\frac{de}{dt} = \frac{\frac{e^2}{\mu} - \lambda}{c} \tag{12}$$

which Cassie combined with circuit equations to arrive at his criterion for "circuit severity."

Low Resistance Arc Regime

With the assumption for the low-resistance arc of the ideal condition previously mentioned, that is, that the circuit is an "infinitely stiff" current source, or $i = i(t)$, *Mayr's* nonlinear equation 8 expressed in terms of R, becomes the linear equation,

$$\frac{d}{dt}\left(\frac{1}{R}\right) + \frac{N_o}{Q_o}\frac{1}{R} = \frac{1}{Q_o}i^2(t), \tag{13}$$

with the solution:

$$\frac{1}{R} = \epsilon^{-t/\theta}\left[\frac{1}{R_o} + \frac{1}{N_o\theta}\int_0^t i^2(t)\epsilon^{t/\theta}dt\right], \tag{14}$$

where the important quantity,

$$\theta = \frac{Q_o}{N_o},$$

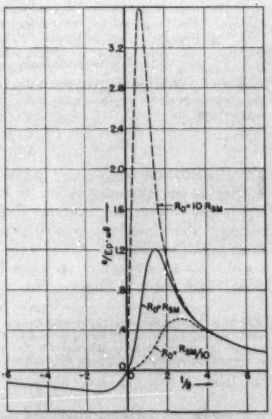


Figure 2. Arc voltage versus time from Mayr's dynamic arc equation for linear current change through zero

Symbols defined under Figure 1

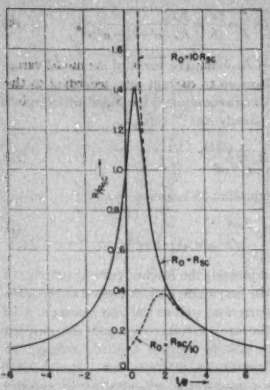


Figure 3. Arc resistance versus time from Cassie's dynamic arc equation for linear current change through zero

R_{sc} = arc resistance at current zero for cyclic "steady state" (Cassie)

$$= \frac{\sqrt{2}E_a}{I_{1\omega\theta}}$$

E_a = arc voltage for $\frac{di}{dt} = 0$

will hereafter be called the "time constant" of the arc and R_o is the value of R at $t = 0$.

For the region of interest, closely embracing current zero, the sinusoidal arc current of peak value, I_1, and angular frequency, ω, can be expressed with sufficient accuracy:

$$i(t) = I_{1\omega t} \left(\text{for } \omega t \ll \frac{\pi}{2}\right) \tag{15}$$

Substituted in equation 14, this gives

$$\frac{1}{R} = \frac{1}{R_{sm}}\left[1 - \frac{t}{\theta} + \frac{1}{2}\left(\frac{t}{\theta}\right)^2\right] + \left(\frac{1}{R_o} - \frac{1}{R_{sm}}\right)e^{-t/\theta} \tag{16}$$

where

$$R_{sm} = \frac{N_o}{2I_1^2(\omega\theta)^2} = \frac{E_p}{2I_1(\omega\theta)^2} \tag{17}$$

and E_p is the arc voltage gradient at the peak current, I_1. Inspection of equation 16 shows that any value of R_o different from R_{sm} will simply add an exponentially decaying transient to the nonexponential term. Therefore, the form of equation 16 for $R_o = R_{sm}$ will be called the dynamic steady state form of the equation. It can also be shown to correspond to the steadily alternating form of the solution of equation 14 for a sinusoidal current in the usual case where $\omega\theta \ll 1$. This steady-state form of equation 16 is plotted as the solid line in Figure 1. The corresponding arc voltage gradient,

$$e = Ri = \frac{E_p}{2\omega\theta} \cdot \frac{\frac{t}{\theta}}{1 - \frac{t}{\theta} + \frac{1}{2}\left(\frac{t}{\theta}\right)^2} \tag{18}$$

is plotted as the solid line in Figure 2. (Since the co-ordinates are dimensionless, the curves represent either per-unit-length or total values for a uniform arc.)

Similarly, *Cassie's* dynamic arc equation becomes, for impressed current,

$$\frac{d}{dt}\left(\frac{1}{R^2}\right) + \frac{2\lambda}{c}\left(\frac{1}{R^2}\right) = \frac{2\lambda}{c} \cdot \frac{i^2(t)}{E_o^2} \tag{19}$$

with the solution,

$$\frac{1}{R^2} = e^{-\frac{2t}{\theta}}\left[\frac{1}{R_o^2} + \frac{2}{E_o^2\theta}\int_0^t i^2(t) \cdot e^{\frac{2t}{\theta}} dt\right] \tag{20}$$

where θ now represents $\frac{c}{\lambda}$, which can be shown to be equivalent to Mayr's Q_o/N_o. $\left(\text{Cassie writes his equations using "}k\text{"} = \frac{\lambda}{2c}\right)$

For

$$i(t) = I_{1\omega t}, \tag{15}$$

equation 20 becomes

$$\frac{1}{R^2} = \frac{1}{R_{sc}^2}\left[1 - 2\frac{t}{\theta} + 2\left(\frac{t}{\theta}\right)^2\right] + \left(\frac{1}{R_o^2} - \frac{1}{R_{sc}^2}\right)e^{-2t/\theta} \tag{21}$$

where

$$R_{sc} = \frac{\sqrt{2}E_o}{I_{1\omega\theta}} = \frac{E_o}{I\omega\theta} \tag{22}$$

in which $I = I_1/\sqrt{2}$ is the root mean square value of the prior current. R_{sc} is, like R_{sm}, the current-zero arc resistance for the dynamic steady state. The "steady-state" variation of Cassie's arc resistance is plotted as the solid line in Figure 3. The corresponding arc voltage gradient,

$$e = Ri = \sqrt{2}E_o \cdot \frac{\frac{t}{\theta}}{\sqrt{1 - 2\frac{t}{\theta} + 2\left(\frac{t}{\theta}\right)^2}} \tag{23}$$

is plotted as the solid line in Figure 4.

The effects of large deviations from the dynamic steady state at current zero, corresponding to the commonly observed erratic behavior of actual arcs, are shown by the dotted lines in Figures 1 to 4. Cathode-ray oscillograms of actual arc reignition voltages often resemble some one of the families of curves of Figures 2 and 4 rather closely, as for example the reignition voltages preceding the extinction transients in the oscillograms of Figure 5. Whenever the arc resistance remains relatively low, however, the resemblance, illustrated by Figure 5*a*, is noticeably closer to Cassie's form of Figure 4 than to Mayr's form of Figure 2. Furthermore, quantitative agreement, to be discussed under the section entitled Comparison with Experiment, between calculated values of R_o and those indicated by oscillograms clearly indicates Cassie's model of the low-resistance arc as the only appropriate model for representing the pre-current-zero regime for ordinary power arcs. The near-constant arc voltage gradient of Cassie's model is also clearly in agreement with the usual magnetic oscillograms of circuit breaker tests.

High Arc Resistance Regime

Just after a critical current zero at which the arc resistance has become so high that its voltage rather than its current is principally determined by the circuit, experience shows that the actual arc, or "arc space," is characterized generally by a rapid further loss of conductivity and a rapid recovery of dielectric strength. The generally used definition of this "dielectric strength" of a still conducting gas space is that given by Slepian[1,2]: the voltage required to hold the conductance of the space constant. According to equation 2, this means the voltage required for a balance between power input and power loss, or for the equality:

$$\frac{e^2}{R} = N \tag{25}$$

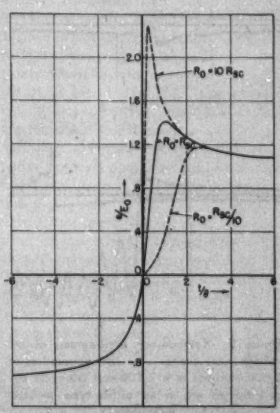


Figure 4. Arc voltage versus time from Cassie's dynamic arc equation for linear current change through zero

From this, the dielectric strength of the arc space according to Slepian's criterion can be defined symbolically as

$$E_d = \sqrt{RN} \tag{26}$$

Cassie's assumptions of constant per-unit-volume resistivity, μ, and power loss, λ, immediately rule out his model for this period, since the dielectric strength per unit length of his model would be

$$E_d = \sqrt{\frac{\mu}{A} \cdot \lambda A} = E_o \tag{27}$$

a constant. Mayr's assumptions, however, lead to

$$E_d = \sqrt{RN_o} \tag{28}$$

a dielectric strength increasing with the square root of the arc resistance, a relation which is at least not contrary to experience. For the high arc-resistance case of circuit-controlled voltage, $e = e(t)$, Mayr's differential equation becomes:

$$\frac{dR}{dt} - \frac{N_o}{Q_o} R = -\frac{1}{Q_o} e^2(t) \tag{29}$$

with the general solution,

$$R = e^{\frac{t}{\theta}} \left[R_o - \frac{1}{N_o \theta} \int_0^t e^2(t) \cdot e^{-\frac{t}{\theta}} dt \right] \tag{30}$$

where, as before, $R_o = R$ at $t = O$ and $\theta = \frac{Q_o}{N_o}$.

In the absence of restored voltage, or

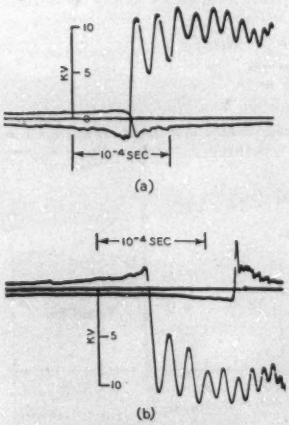


Figure 5. Cathode-ray oscillograms of arc voltage near current zero for interruption of 3,600 amperes in a 7,100-volt 60-cycle circuit by an arc in an orifice-type air-blast interrupter

5*a* and *b* are successive tests at 95 pounds per square inch air tank pressure

where the square of the restored voltage can be neglected, equation 30 shows that

$$R = R_o e^{t/\theta} \tag{31}$$

the arc resistance approaching infinity exponentially at a rate depending only upon R_o and θ. Slepian's dielectric strength becomes

$$E_d = \sqrt{N_o R_o} e^{t/2\theta} = E_o e^{t/2\theta} \tag{32}$$

(The identity, $E_o \equiv \sqrt{N_o R_o}$, follows from equations 27 and 28.) Clearly, equation 32 must be limited to the range where the assumptions underlying it are tenable. It may not, for instance, have any validity for arc reignitions occurring at times very much greater than θ after current zero where arc reignition takes on the aspect of ordinary sparkover. Thus, it might apply to the early rapidly rising portions of experimentally determined dielectric recovery curves[7] but not to the plateau region following the rapid recovery.

Arc Model Behavior After Current Zero With Various Forms of Restored Voltage

Constant Voltage

$e = E_1$

Equation 30 becomes

$$R = \frac{E_1^2}{N_o} + \left(R_o - \frac{E_1^2}{N_o} \right) e^{t/\theta} \tag{33}$$

or

$$R = R_o \left[\frac{E_1^2}{E_o^2} + \left(1 - \frac{E_1^2}{E_o^2} \right) e^{t/\theta} \right] \tag{34}$$

For $E_1 = E_o$, the exponential term vanishes and R remains constant at R_o. This, however, is an unstable equilibrium, since R approaches zero (the arc "reignites") for any excess of E_1 over E_o, and conversely, R approaches infinity (the arc disappears) for any E_1 less than E_o. If the value of E_1 which is critical for extinction is called E_c, equation 34 shows that for this case of constant applied voltage,

$$E_c = E_o \tag{35}$$

If the ratio of E_1 to E_c is called α, equation 34 can be conveniently written

$$R = R_o [\alpha^2 + (1 - \alpha^2) e^{t/\theta}] \tag{36}$$

From equations 36 and 28, the dielectric strength of the arc space according to Slepian's definition can immediately be written

$$E_d = E_o \sqrt{\alpha^2 + (1 - \alpha^2) e^{t/\theta}}, \tag{37}$$

reducing to equation 32 when $\alpha = O$.

The arc residual or "leakage" current is

$$i = \frac{e}{R} = \frac{\alpha E_o}{R} = \frac{E_o}{R_o} \cdot \frac{\alpha}{\alpha^2 + (1 - \alpha^2) e^{t/\theta}} \tag{38}$$

If R_o, being the result of the model variation up to current zero according to the Cassie equation, is identified with Cassie's "steady-state" value,

$$R_{sc} \equiv \frac{\sqrt{2} E_o}{I_{1\omega\theta}} \tag{22}$$

equation 38 becomes

$$i = \frac{I_{1\omega\theta}}{\sqrt{2}} \cdot \frac{\alpha}{\alpha^2 + (1 - \alpha^2) e^{t/\theta}} \tag{39}$$

expressing the residual current in terms of the amplitude and frequency of the prior current as well as the time constant, θ, of the arc's conductivity and the ratio of actual to critical applied voltage, α Specifically, it states, for instance, that for exactly the critical value of restored voltage ($\alpha = 1$), the arc current will remain constant at $\omega\theta$ times the rms value of the prior arc current.

Likewise, the power input to the arc space can be written:

$$W = ei = \frac{E_o I_{1\omega\theta}}{\sqrt{2}} \cdot \frac{\alpha^2}{\alpha^2 + (1 - \alpha^2) e^{t/\theta}} \tag{40}$$

All of these quantities are plotted in dimensionless form in Figure 6 for values for the parameter α: 0, 0.707, 1.0, 1.01, 1.1, and 1.414, showing, for this composite model, the variation of these quantities with time after current zero at zero, critical and near-critical values of a constant restored voltage. The short section of curve to the left of the origin in each case represents, for comparison, the assumed model variation just before current zero, calculated according to Cassie's equation with impressed linear current fall.

Constant Rate of Rise

$e = E_1 \nu t = \alpha E_c \nu t$

In a manner similar to that used in the preceding section, it can be shown by means of equation 30 that E_c, the critical value of the voltage amplitude E_1 for arc extinction (determining the boundary between ultimate approach of the arc resistance to zero or to infinity), is

$$E_c = \frac{E_o}{\sqrt{2}\, \nu\theta}, \tag{42}$$

or that the critical rate of rise of voltage is

$$E_c \nu = \frac{E_1}{\sqrt{2}\, \theta}. \tag{43}$$

In terms of

$$\alpha = \frac{E_1}{E_c} = \frac{E_1 \nu}{E_c \nu},$$

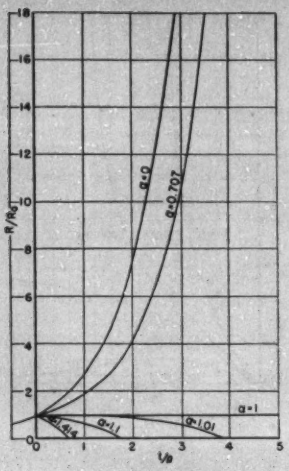


Figure 6a—Resistance versus time
R_o = resistance at current zero = R_{sc}
θ = time constant of arc column

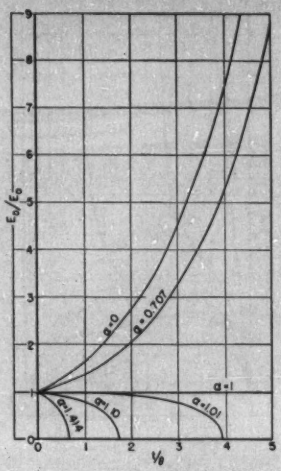


Figure 6b—Dielectric strength versus time

E_o = arc voltage for $\frac{di}{dt}=0$ in preceding half-cycle

Figure 6. Behavior of composite arc model with constant restored voltage,

$e = E_1 = \alpha E_c$

where E_c is the critical value of restored voltage for arc extinction

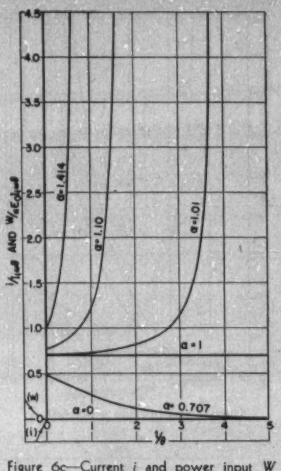


Figure 6c—Current i and power input W versus time

$I_{1\omega}$ = rate of current fall just before current zero

the ratio here of actual to critical rate of voltage rise, the arc resistance equation from 30 is

$$\frac{R}{R_o}=\alpha^2\left[1+\frac{t}{\theta}+\frac{1}{2}\left(\frac{t}{\theta}\right)^2\right]+(1-\alpha^2)e^{t/\theta} \qquad (44)$$

and the dielectric strength is

$$E_d=E_o\sqrt{\alpha^2\left[1+\frac{t}{\theta}+\frac{1}{2}\left(\frac{t}{\theta}\right)^2\right]+(1-\alpha^2)e^{t/\theta}} \qquad (45)$$

Likewise, the arc "leakage" current is

$$i=\frac{e}{R}=\frac{\alpha I_{1\omega}\theta}{2}\cdot\frac{\frac{t}{\theta}}{\alpha^2\left[1+\frac{t}{\theta}+\frac{1}{2}\left(\frac{t}{\theta}\right)^2\right]+(1-\alpha^2)e^{t/\theta}} \qquad (46)$$

and the arc power input per unit length is

$$W=ei=\frac{\alpha^2E_oI_{1\omega}\omega\theta}{2\sqrt{2}}\cdot\frac{\left(\frac{t}{\theta}\right)^2}{\alpha^2\left[1+\frac{t}{\theta}+\frac{1}{2}\left(\frac{t}{\theta}\right)^2\right]+(1-\alpha^2)e^{t/\theta}} \qquad (47)$$

These relations are plotted in Figure 7 with values of the ratio α of 0, 0.707, 1, 1.1, and 1.414. In Figure 7*b* the applied voltage rise line is drawn dotted for the critical case, $\alpha=1$, illustrating how the dielectric recovery curve for $\alpha=1$ lies almost parallel with the applied voltage line, approaching it asymptotically at infinity. Sections of the applied voltage lines for $\alpha=1.1$ and $\alpha=1.414$ are shown intersecting the corresponding dielectric recovery curves at their peaks where, as called for by the definition, the rate of change of arc resistance and therefore of dielectric strength is zero. When the applied voltage exceeds the dielectric strength, the latter soon collapses to zero (assuming the circuit to be an infinitely stiff voltage source). With finite circuits this collapse would, of course, mark the return of the "low resistance regime" where current rather than voltage is determined by the circuit.

Figure 7*c* shows the computed effect of the varying arc resistance on the course of the current with the various rates of rise of restored voltage. At critical and just below critical rates of rise the model residual currents rise to a peak of a few tenths of the prior arc current peak times $\omega\theta$ at a time close to θ and then decay to zero in an exponential manner. At greater than critical rates of rise the current merely experiences a more or less sharp deviation in slope for a time of the order of θ before suddenly rising toward infinity (approaching the undeviated current line with a finite circuit).

The model power input curves of Figure 7*d* emphasize the importance of α in determining the energy balance of the arc. At rates of rise less than 70 per cent of critical, the power input to the arc remains so far short of the assumed steady loss that the arc's loss of conductivity and its dielectric recovery are little different from what they would be with no restored voltage, as can be observed in Figures 7*a* and 7*b*. On the other hand, an early and sharp upturn characterizes the power curves for rates of voltage rise only slightly in excess of critical. For a finite circuit, of course, the power curves for arc reignition would necessarily reach a maximum and then fall to or near the power loss level upon a return to the circuit-limited-current or low-arc-resistance regime.

In Figure 7*e* are plotted, along with the static characteristic, the dynamic volt-ampere characteristics corresponding to the various values of α for the linear voltage rise case. Time is expressed as a parameter indicated by the location along the curves of the numbered dots. Such plots, with a little interpretation, can give a very complete picture of the arc behavior near current zero and are especially instructive when compared with the static characteristic.

The latter can be plotted from a family of reignition volt-ampere

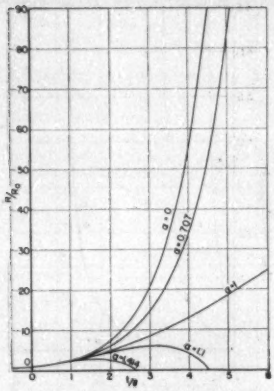


Figure 7a—Resistance versus time

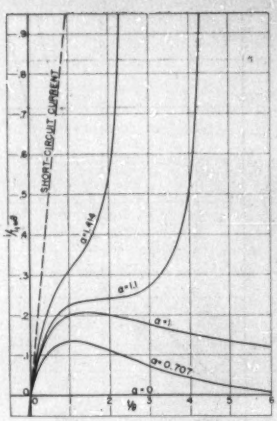


Figure 7c—Current versus time

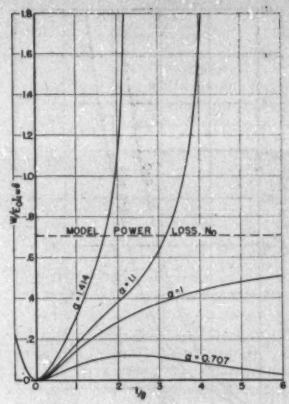


Figure 7d—Power input versus time

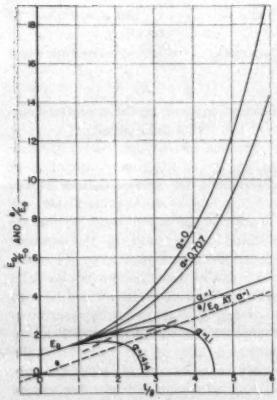


Figure 7b—Dielectric strength versus time. Voltage rise lines shown dotted

Figure 7e—Voltage versus current (dynamic and static voltampere characteristics)
Time shown as a parameter by spacing of dots for integral values of t/θ

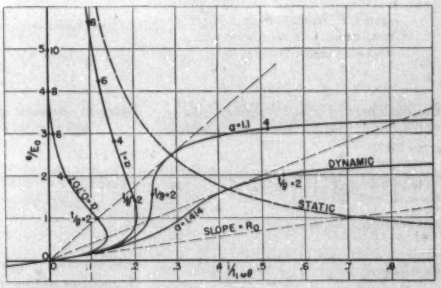


Figure 7 (a,b,c,d,e). Behavior of composite arc model with constant rates of voltage rise (approximation to the case of the resistance shunted arc)

α = ratio of actual to critical rate of voltage rise for arc extinction

curves at various rates of voltage rise by connecting in a smooth curve points at which the dynamic curves have tangents passing through the origin. The arc resistances are given by the slopes of lines from any point to the origin, dielectric strengths are the voltages at the intersections of these lines through the origin with the static characteristic, and power inputs are the products of the point co-ordinates. The common slope with which all the dynamic curves pass through the origin is, of course, R_o, the initial arc resistance. Such plots, traced out by a single cathode-ray oscillograph beam provided with timing markers, thus offer attractive possibilities for studying the near-current-zero behavior of actual arcs.

Square Law Rate of Rise

$$e = \frac{E_1}{2}(\nu t)^2 \tag{48}$$

For a complete mathematical description of the restored voltage with actual circuits, much more complex expressions than those of the preceding sections are required. Better approximations for certain still idealized cases are as follows: In a purely resistive circuit where the initial ($t=0$) current and voltage are assumed to be zero,

$$e = E_1 \sin(\nu t), \tag{49}$$

where ν is the angular frequency ($2\pi f$) of the supply generator and so is the same as ω for the prior arc current. In a purely inductive circuit with pure resistance r shunting the arc,

$$e = E_1(1 - \epsilon^{-\nu t}) \tag{50}$$

where

$$\nu = \frac{r}{L},$$

the reciprocal of the circuit time constant, is large compared with the generator angular frequency, ω. At small values of νt, both of these forms can be approximated by the linear rise expression of the preceding section. However, for the arc in a purely inductive circuit shunted by capacitance, the idealized voltage recovery is given by

$$e = E_1[1 - \cos(\nu t)], \tag{51}$$

having a zero initial rate of recovery. Equation 48 of this part is the short-time approximation for this form.

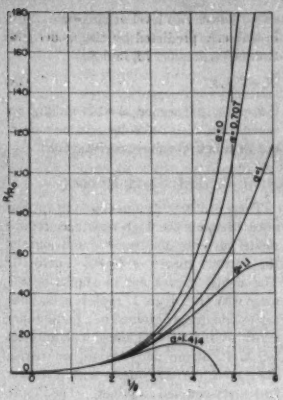


Figure 8a—Resistance versus time

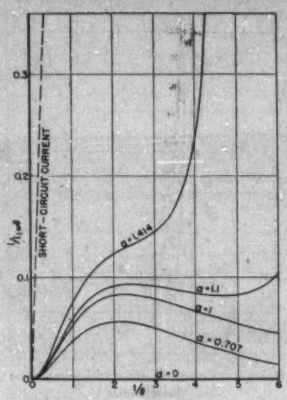


Figure 8c—Current versus time

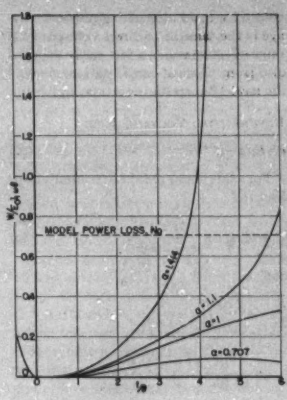


Figure 8d—Power input versus time

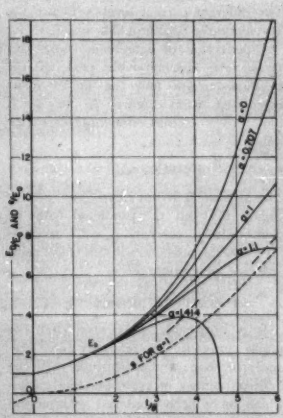


Figure 8b—Dielectric strength versus time

Figure 8e (right)—Voltage versus current

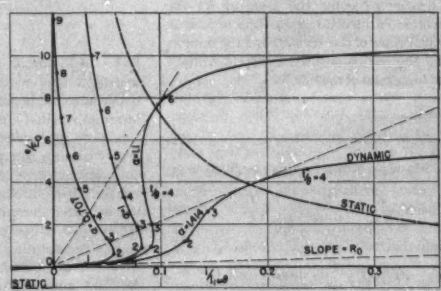


Figure 8. Behavior of composite arc model with voltage rising as the square of the time (approximation to the idealized case of the capacitance shunted arc)

α = ratio of actual to critical amplitude of voltage rise for arc extinction

Carrying out the integration of equation 30 for this case shows that the critical amplitude,

$$E_c = \frac{1}{2}\sqrt{\frac{2}{3}}\cdot\frac{E_0}{(\theta\nu)^2} \tag{52}$$

and that the arc resistance equation is

$$\frac{R}{R_0} = \alpha^2\left[1+\frac{t}{\theta}+\frac{1}{2}\left(\frac{t}{\theta}\right)^2+\frac{1}{6}\left(\frac{t}{\theta}\right)^3+\frac{1}{24}\left(\frac{t}{\theta}\right)^4\right]+(1-\alpha^2)\epsilon^{t/\theta} \tag{53}$$

where, as before, E_c is the critical value of E_1 for arc extinction and α is the ratio of E_1 to E_c. From equation 53, the equations for dielectric strength, current, and power input can be derived as before. Figure 8 is a plot of all these relations for this case with the same values of α as in Figure 7.

Again, the curves in Figure 8*b* illustrate the parallelism of the voltage and dielectric strength curves for the critical case (α = 1) and the divergence or convergence of these curves for α less than or greater than one, respectively. Figure 9, which is a replot from Figure 7*e* and 8*e* to a common and extended scale, shows the comparative behavior of the model arc under critical conditions with the resistance and with the capacitance-shunted types of early voltage recovery. It also shows more clearly than before the relation between these dynamic characteristics and the assumed static characteristics for the model, both before and after current zero.

Sine Law Voltage Rise

$$e = E_1 \sin(\nu t) \tag{49}$$

The integration of equation 30 for this form has already been shown by Mayr[4] to yield an

$$E_c = E_c\sqrt{2+\frac{1}{2(\theta\nu)^2}} \tag{54}$$

and an arc resistance equation

$$\frac{R}{R_0} = \alpha^2\left[1+\frac{2\theta\nu\sin(2\nu t)-\cos(2\nu t)}{1+(2\theta\nu)^2}\right]+(1-\alpha^2)\epsilon^{t/\theta} \tag{55}$$

with the other pertinent relations derivable in a straightforward manner. The arc model behavior can be seen to be a function not only of t/θ and α but of a new parameter, $\theta\nu$, making its graphical representation more complex. For this reason, and also because at the longer times, when this and the following voltage rise equations are needed, the effects of departures from the assumed conditions would be cumulative, the relations for this and the following cases have not been plotted.

A significant deduction from equations 54 and 55 is that at very high frequencies ($2\theta\nu \gg 1$) the response of the model arc

resistance to an alternating restored voltage is the same as to direct voltage of the same effective value, a fact easily deduced also from physical considerations for any arc model incorporating a time lag.

Exponential Voltage Rise

$$e = E_0(1 - \epsilon^{-\nu t}) \tag{50}$$

For this case, Mayr's computations lead to:

$$E_c = E_o\sqrt{1 + \frac{3}{2\theta\nu} + \frac{1}{2(\theta\nu)^2}} \tag{56}$$

and

$$\frac{R}{R_o} = \alpha^2\left(\frac{E_c}{E_o}\right)^2\left[1 - \frac{2}{1+\theta\nu}\epsilon^{-\nu t} + \frac{1}{1+2\theta\nu}\epsilon^{-\left(\nu + \frac{1}{\theta}\right)t}\right] + (1-\alpha^2)\epsilon^{t/\theta} \tag{57}$$

with the other relations following as before. It can be easily shown that these equations become the same as for the section entitled Constant Rate of Rise at small rates of rise ($\theta\nu \ll 1$) and the same as for the section entitled Constant Voltage at high rates of rise ($\theta\nu \gg 1$).

Oscillatory Voltage Rise

$$e = E_0[1 - \cos(\nu t)] \tag{51}$$

Mayr's solution for this case leads to:

$$E_c = E_o\sqrt{\frac{2}{3}\left[1 + \frac{5}{4(\theta\nu)^2} + \frac{1}{4(\theta\nu)^4}\right]} \tag{58}$$

and

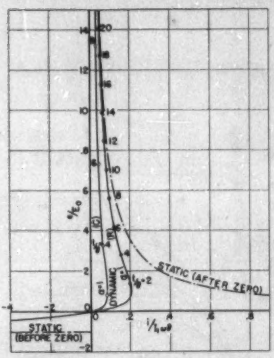
$$\frac{R}{R_o} = \frac{\alpha^2}{2}\left(\frac{E_c}{E_o}\right)^2\left[3 + 4\frac{\theta\nu\sin(\nu t) - \cos(\nu t)}{1+(\theta\nu)^2} - \frac{2\theta\nu\sin(2\nu t) - \cos(2\nu t)}{1+(2\theta\nu)^2}\right] + (1-\alpha^2)\epsilon^{t/\theta} \tag{59}$$

from which the corresponding relations for dielectric strength, current, and power input can be deduced. Again, it can be shown that the resistance equation 59 reduces to that for the section entitled Square Law Rate of Rise at short enough times ($\nu t \ll \pi/2$ and $t/\theta \ll 1$) and to that for the section entitled Constant Voltage for high enough frequencies ($\theta\nu \gg 1$).

Comparison With Experiment

Low Arc Resistance Regime

The arc voltage reversal, or "reignition transient," preceding the final, or "extinction transient," in Figure 5*a* can be used as a rough quantitative check of the chosen arc model behavior under the continuing condition of relatively low arc resistance. From Figure 4, the re-established voltage peak should occur near


STATIC (AFTER ZERO)
STATIC (BEFORE ZERO)
DYNAMIC
$\theta=1$
$\theta=2$
$\theta=4$
$\theta=8$
$\theta=16$
$\theta=20$
$\theta=12$
$\theta=10$
$\theta=14$
$\theta=18$
$\theta=6$
$\theta=8$
$\theta=10$
$\theta=12$
$\theta=14$
$\theta=16$
$\theta=18$
$\theta=20$
$\theta=2$
$\theta=4

ance than predicted by the model. This difference in timing between the model and actual arcs can easily account for discrepancies of as much as ten to one, for example, between observed and predicted values of arc resistance at current zero. Because of the relation, $E_a = \sqrt{R\ N}$, corresponding discrepancies in dielectric strength may be only three to one. Studies of these discrepancies between the theoretical model and actual arc behavior should aid greatly in achieving a better understanding of actual arc processes.

Discussion

Since this study concerns the arc column alone, it cannot, of course, be applied to short-arc switches such as low-voltage relays and contactors or metal plate "De-ion" air circuit breakers. It may be applied to interrupters employing long arcs, where the characteristics of the arc column predominate. These include all gas-blast and magnetic-blast circuit breakers—not excepting those employing liquid or solid gas evolving media—expulsion fuses and protective gaps, and long power arcs in the open air. The study is generally of interest only in the higher ranges of current where the arc does not completely "go out," or lose its conductance, before a current zero.

Of greatest practical interest are the curves of Figure 7 for constant rates of rise of recovery voltage. Examination of cathode-ray oscillograms like those of Figure 5 shows that, even when the restored voltage is oscillatory, passage of the voltage through zero may be approximately linear. The square-law type of voltage rise of Figure 8 is approached only with comparatively large values of shunting capacitance. (Illustrated by Figure 4*d* of Reference 7.)

As an example of a possible use of these curves and equations, oscillographic data such as that of either Figure 5 or Figure 11, permitting an estimate of θ for a particular arc condition, may be used, along with the usual test data from magnetic oscillograms, to construct a complete model of the arc for use as a starting point for quantitative estimates. For instance, a critical rate of voltage rise for arc extinction may be computed from equation 43,

$$\left.\frac{de}{dt}\right|_c = E_a\nu = \frac{E_a}{\sqrt{2}\theta}$$

or the current-zero arc resistance for comparison with the required value for critically damping circuit oscillations may be estimated by means of equation 22,

$$R_{ac} = \frac{E_a}{I\omega\theta}$$

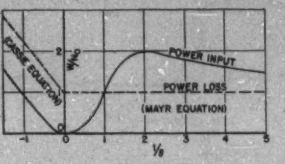


Figure 10. Power flow in composite arc model with linear passage of current through zero

$(i = I_{1\omega}t)$

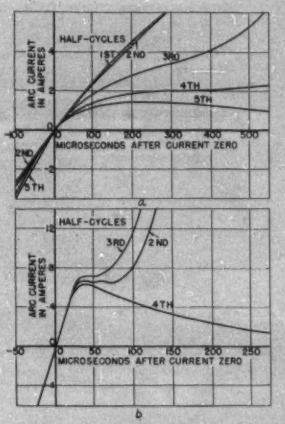


Figure 11. Replots of cathode-ray oscillograms of arc current magnitude versus time near current zero for actual interruption tests

Figure 11*a*—Interruption of 50 amperes in 5 half-cycles in a 0.625-inch by 4-inch fiber fuse tube, 1,530-volt circuit, $E_a = 290$ volts

Figure 11*b*—Interruption of 507 amperes in 4 half-cycles in a 0.625-inch by 4-inch boric acid fuse tube, 1,680-volt circuit, $E_a = 330$ volts

Such computations are aided by the tendency in many cases for θ to remain about the same for a given arc arrangement, even though such conditions as gas pressure[2] may undergo wide variation. Theoretical calculations of θ, based on the defining relations,

$$\theta = \frac{Q_o}{N_o} \text{ or } \theta = \frac{c}{\lambda}$$

are possible but the practical reliability of such calculations has not yet been demonstrated.

Being based on generally similar premises, the dielectric recovery equations and curves for small energy input ($\alpha \ll 1$) resemble closely those predicted earlier by Slepian[1,2] from the assumption of conductivity loss by diffusion of ions. Therefore, deductions with regard to practical devices made then are still applicable. The assumption of equation 2, that arc conductance is a function of energy content alone, reduces the generality of these calculations compared to those of Slepian but available checks with observed dynamic arc behavior seem to justify this assumption for practical purposes. It should be noted, of course, that the model equations do not actually depend on any specific mechanisms of gain or loss of energy and conductivity but only on the rather general assumptions as to functional forms expressed by the equations 2, 5, 6, 10, and 11. Any natural processes which conform even approximately to these relations would explain arc behavior resembling that of the model. As an example of insensitivity to specific assumptions, it may be noted that, at zero energy input, the equations of Mayr and Cassie for rise of arc resistance (equation 31) become identical and their curves have a very similar appearance even as critical voltage recovery is approached.

The justification for speaking of a unique dielectric recovery curve under practical conditions where interrupters usually enjoy a factor of safety is clear from curves like those of Figures 7*a*, 7*b*, 8*a*, and 8*b*, which show the small effects on rise of resistance and dielectric strength of the model arc of recovery voltage amplitudes less than 70 per cent of critical. It is also shown, however, that attempts such as have been made in the past[7] to determine these dielectric recovery curves from data obtained only by near-critical tests may fail, or at least may give pessimistic results. Thus, dielectric recovery curves from critical tests with capacitance shunts may be expected to differ materially in shape and magnitude from those obtained with resistance shunts, even for the same interrupter in the same supply circuit. (Compare curves for $\alpha = 1$ in Figures 7*b* and 8*b*.)

In addition to the uses already suggested, this or similar models may also be used for the mathematically more difficult investigation of the mutual interaction between the arc and the circuit as already attempted by Cassie[9] and Mayr.[4,5] More experimental studies like those by Lewis[8] of near-current-zero arc currents and resistances in various types of interrupters under various circuit conditions are urgently needed to establish sounder bases for arc calculations; but preliminary calculations like those described here are, in turn, of value in guiding such experimental studies, which should lead to far better understanding of circuit

interruption phenomena and, as a result, to sounder design, rating, and application of circuit interrupting devices.

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Discussion

Laurel J. Lewis (University of Washington, Seattle, Wash.): The study of physical phenomena by analytical methods even under idealized conditions, as in the present paper, is particularly valuable in extending our understanding of complicated relationships. It is gratifying to note the considerable advancement that has been achieved in study of arc behavior, as is evidenced by a comparison of the present "models" used for analysis and the simpler ones in use only a few years ago. It seems appropriate, however, to emphasize the limitations of measurements of external arc characteristics in determining what goes on internally in the arc space. Certainly Browne will agree that correlation between mathematical equation and test result must not be taken too literally in interpreting the mechanism of arc conductivity. For example, in those cases where the precurrent-zero variations are represented best by Cassie's equations, based on a variable radius of the positive column, and the variations following current zero are best represented by Mayr's equation based on a constant column radius and variation in temperature throughout the cross-section, it would be improper to conclude that such changes in the positive column are thereby demonstrated. We still have a long way to go in knowledge of the arc mechanism, which can only be determined by physical research under rigidly controlled conditions.

It would seem that one of the most vital questions concerning the knowledge of arc behavior centers about the conditions existing precisely at the instant of current reversal. In Figure 11*a*, the current curves all appear to be tangent at the current zero points. Perhaps Browne could indicate how accurately the zero position is known for these oscillograms. In the writer's own investigation, a significant change in slope of the current curves was sometimes noted prior to reversal, and the slopes were not the same at the zero position. A related point is whether the current and voltage zeros are coincident, since in the application of the "models," in all cases except as given under the section entitled "Arc Model Behavior After Current Zero with Various Forms of Restored Voltage," the voltage has been taken as starting at zero, coincident with the current. The evidence in the writer's study seemed to indicate some time difference in the zero values. If this is really so, the concept of "resistance" as applied to the arc space needs a different interpretation than that usually employed, inasmuch as the arc space acts as a source rather than as a sink of energy during a brief period, and the result is a discontinuity in the resistance.

It is noted that all the curves calculated by Browne for the high resistance regime applicable to interruption ($\alpha > 1$), the resistance increases approximately exponentially with time from an initial value R_e. It was again the conclusion of the writer, for the test circuit studied, that even at the last half-cycle where clearing was finally effected, there was first a period, perhaps as long as 25 microseconds, during which the ratio of arc voltage to current decreased as the current built up to a maximum, after which the "resistance" began to increase as the current decreased to zero. For cases where restrikes occurred, the ratio of voltage to current decreased continually until the normal arc was re-established. Under these conditions it seems questionable whether one should consider the dielectric strength or critical gradient in a "static" sense as being related to a voltage and a resistance at a specific instant of time. Rather the question of whether or not breakdown is to occur for a given voltage at a certain instant, is related to the resistance that existed some time previously and to the current that has been permitted to flow to furnish the energy necessary to enhance the conductivity. The situation is somewhat analogous to that in impulse testing, where the time of application of voltage is important.

W. G. Hoover (Stanford University, Stanford, Calif.): This very interesting paper is a definite contribution to the understanding of arc phenomena in circuit breakers. It helps to clarify the explanation of the influence of circuit recovery characteristics upon arcing time.

It would be of interest for the author to discuss the relative applicability of his derivations to the wide range of rated circuit breaker voltages and interrupting currents covered by present practice. As a particular question, in high-voltage high-speed, short arcing time circuit breakers, does the rapid lengthening of the arc and powerful deionizing action before current zero modify significantly the relationships developed in this paper?

W. F. Skeats (General Electric Company, Philadelphia, Pa.): The author has presented an attempt at mathematical analysis of arc properties in the neighborhood of current zero on the basis of a combination of relationships assumed by two earlier authors. It seems pertinent to point out that there are certain peculiarities in both of these sets of assumptions. Mayr, for instance, assumes an arc column whose temperature varies with time, but whose power loss is constant; while Cassie assumes a circular section with power loss throughout but no radial temperature gradient and no change in energy content per unit volume. That the change in power loss in Mayr's case, and the temperature gradient and change in energy content in Cassie's case, are negligible is possible but certainly not obvious.

The separation of the problem into one regime in which the outside circuit may be considered as controlling current with relatively little effect from arc voltage and a second in which the outside circuit controls voltage with relatively little effect from arc conductance is a useful device as long as the phenomena in question are limited to these two regimes. There is, however, an intermediate regime in which neither voltage nor current is independent of the properties of the arc and the fact that there are so many cases in which the recovery transient has been observed to be substantially affected by post-arc conductivity indicates that very often the phenomena fall within this regime. The results of any analysis avoiding this regime, therefore, are definitely limited.

This is illustrated in the author's Figures 8*b* and 8*c* in which the slopes of both current and voltage show a sharp decrease as the current passes through zero. The usual circuit will permit an increase in the voltage slope accompanied by a decrease in the current slope, or vice versa, if the properties of the arc undergo a suitable change at that time; but no circuit comes to mind, certainly not that of the idealized capacitance-shunted arc to which these curves are intended to apply, which will permit a simultaneous sharp decrease in both quantities.

The author discusses at some length the subject of dielectric strength, noting that its variation with time may be quite different for different applied voltage transients. At one point, he even utters a warning that dielectric recovery curves obtained from near critical tests may give misleading indications as to the shape of these curves under recovery voltage conditions well below the critical. Is it to be implied from this concern that it is more important to know the dielectric strength of a gap that is only lightly stressed than to know the strength of a gap stressed nearly to failure? Perhaps it would be well to consider that since the dielectric strength as defined by the author describes the response of the circuit breaker to a type of transient that rarely, if ever,

occurs in the course of circuit breaker operation, it is an item of rather academic interest. From the practical standpoint the curves for the critical value of applied voltage seem much more significant.

In view of the foregoing limitations together with those which the author himself has mentioned, it seems quite premature to suggest that the measurement of such quantities as the author has mentioned should be used for determining the limiting rate of rise at which a circuit breaker will perform satisfactorily. In spite of the difficulties associated with applying the desired rate of rise under all circumstances, it appears that that procedure will be more satisfactory and reliable for some time to come. The use of R_{86} for determination of critical damping has the further objection that even according to the author's picture it is not this resistance, but a resistance occurring substantially later, and therefore quite different, which determines damping conditions.

There is no disagreement with the author's statement of the need for more data on the performance of actual circuit breakers in this respect, and it should be emphasized that it is desirable to obtain such data on circuit breakers quite similar to those on which it is to be applied as the characteristics of different circuit breakers may be quite different in this respect.

W. M. Leeds (Westinghouse Electric Corporation, East Pittsburgh, Pa.): Circuit interrupting devices, which comprise an important class of electrical equipment, have so far defied most attempts at exact design calculation, particularly with respect to interrupting capacity. However, one noteworthy example of precalculated circuit breaker performance is in connection with the metal plate De-ion air circuit breaker for ratings as high as 25,000 volts. The theory and the structural development of this circuit breaker were described in two papers presented to the Institute in 1929 by Slepian,[1] Dickinson and Baker.[2] The fundamental principle was based on the experimental fact that a short a-c arc between metal plates requires a reignition voltage of at least 200 volts crest if the arc is to continue beyond a normal cyclic current zero. This minimum dielectric strength recovery in the gap is attributed to the positive ion space charge layer at the cathode. By splitting up a single arc into a multiplicity of short arcs between metal plates, each with its own cathode space charge layer, high voltage circuit breakers were designed successfully to interrupt at the first current zero after the arc transfer to the plate structure has taken place.

Another attempt to reduce circuit breaker design to a mathematical formula is represented by the "displacement theory" of circuit interruption. As first enunciated by Prince and Skeats[3] in 1931 in connection with an oil blast breaker, the criterion of successful circuit interruption is that the impulse strength of a fluid dielectric in volts per inch multiplied by the velocity in inches per second at which a growing film of this dielectric is introduced following a current zero shall be equal to or exceed the rate of rise of recovery voltage in volts per second appearing across the contact gap.

Circuit breaker designs built in accordance with the oil blast principle have shown greatly improved performance. However, Slepian has demonstrated in his paper[4] presented in 1940 that the idealized model presented by the displacement theory is impossible according to accepted hydrodynamical theory. Furthermore, the experimental evidence does not confirm quantitatively the predicted linear relationship between voltage recovery rate and critical fluid flow velocity. As a result, the exact mechanism by which arc reignitions are prevented has remained somewhat vague and confused, reminding one of the turbulence in the arc space which seems characteristic of the phenomena under discussion.

Back in 1933 Van Sickle and Berkey presented a paper[5] giving experimental evidence of leakage currents following current zero in high power circuit breakers obtained with cathode ray oscillograms showing definite damping action on the recovery voltage transients. Through this work and that of other researchers the evidence has been accumulating to show that post-arc conductivity and the constants of the circuit are interrelated in a definite manner, particularly when approaching the limit of arc rupturing ability.

The author is to be commended for bringing to the attention of the Institute the important analytical work of Cassie and Mayr in this field. Cassie's criterion of circuit severity, indicating a direct proportionality between the critical air pressure required by an air blast circuit breaker and the arc current multiplied by a special type of impedance function, seems to agree surprisingly well with experimental data. It is important to note that Cassie's impedance function is determined both by the circuit constants and the characteristic time constant of the arc discussed in this paper.

By means of a dual idealization of the arc column, differentiating between conditions before and after current zero, the author has obtained a mathematical formulation which appears to follow the observed behavior of an arc during the extinction process with considerable accuracy. Although most of this analysis has been applied either to only moderately effective oil interrupters or to air circuit breakers which characteristically show appreciable arc space conductance for a short time after current zero, it is expected that the fundamental principles apply equally well to the most effective oil circuit breakers with their usually more abrupt transition from conductor to insulator at current zero.

Regardless of the mathematical precision of predictions based on idealized arc models, any clarification of the basic theory underlying the interrupting process is of assistance in guiding circuit breaker design with increased reliability.

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B. V. Hoard (Bonneville Power Administration, Portland, Ore.): The design of most electrical equipment is usually considered to be a science, but when the problem of interrupting an electric circuit is involved, whether by circuit breaker, fuse, or other means, knowledge of arc phenomena under various conditions is so limited that the design of this equipment is still more of an art than a science. The development of usable and illustrative mathematical expressions in Browne's paper, which indicate the probable behavior of an a-c arc near current zero under various conditions, is an important advancement in helping to understand this complicated phenomena.

This knowledge in turn should be applied to obtain better performance characteristics for fuses and circuit breakers. Electric power systems are growing rapidly, and there is need for very large circuit breakers on these systems that will have improved characteristics and reliability. For example, while circuit breakers having 10,000,000-kva interrupting rating are to be installed in the near future at the Grand Coulee power plant, exploratory studies of future system arrangements indicate that 10 to 15 years from now as more lines, loads, and generating stations are added to the system of the Bonneville Power Administration, circuit breakers having 12,500,000 to 15 million-kva interrupting rating will be required at Grand Coulee if all 18 generators are directly paralleled. It is expected that as the system grows, circuit breakers of 7,500,000 and 10,000,000-kva interrupting rating will be required at several substations, and larger circuit breakers may be required at new hydro generating stations, plans for which are now being formulated.

With such large concentrations of power and of interrupting duty on circuit breakers, it is our belief that any advancement which will make it possible to make the design of interrupting equipment more nearly a science rather than an art is well worth while. Browne's paper is an important step in this direction, and we hope that in the near future other additional steps will be taken.

A. M. Cassie, nonmember (Electrical and Allied Industries Research Association, Greenford, Middlesex, England): May I congratulate the author on a very clear and illuminating account of recent attempts to formulate the dynamic properties of the arc and to apply the results to the extinction of arcs in circuit breakers. He has himself made no mean contribution to the subject by combining the two forms of dynamic equation which have been proposed, so presenting a clear account of a problem which has proved baffling, and is still far from having been completely solved. It would appear that, until more experimental data become available, this composite representation is perhaps about as far as we can usefully go by calculation. So far the main value of these theories has been to show the type of information which is required and the lines along which further investigation ought to be carried out.

In the hope that it may prove useful, I should like to amplify a little what was said in my paper on this subject to the Paris Conference in 1939. That was, of necessity, brief to the point of obscurity in several respects, particularly in regard to the mode of application of the dynamic equation. My first attempts to set up such a dynamic equation visualized, as does Mayr, a hyperbolic static characteristic. The difficulty about such an equation, besides the purely mathematical one, is that the time constant in the dynamic equation proves to be a function of the arc diameter. Mayr has not really overcome this difficulty, and, by limiting arc losses to conduction only, precludes the static—and perhaps dynamic—characteristics which apply to the greater portion of the duration of the circuit breaker arc, where convection is the major loss process. Mayr's equation is in fact, as the author points out, limited in its application to the period, say, 10 to 100 microseconds after current zero. The consideration of the physical state of the arc column at, and immediately after, current zero, which led to the theory put forward in the Paris paper, suggested the possibility that, by assuming the steady state voltage, e_a, as nearly constant over that period, and by regarding that period as critical, it might be possible to work out a critical equilibrium state between arc and circuit. The author is, of course, perfectly correct when he points out, equation 27, that such a dynamic equation leads to a constant final dielectric strength too small for the circuit breaker to extinguish. The most striking consequence of the equation at this stage was to show that, even for an infinitely steep restriking voltage transient, in the form of a unit function, $E1$, the dielectric strength would not need to reach the value E_1 until a time comparable with a few times $1/k$ or θ after current zero. Step by step calculations of resistance and energy-time curves showed just how slow the rise in e_a need be, and encouraged the hope that the project of combining the simplified dynamic equation with those of the circuit might bear fruit.

It is here that the author's conception of the critical stage in arc extinction and that put forward in the Paris Conference part company. To make this clearer, we can think of the extinction period as divided into four parts. First there is the period when the circuit controls the current through the arc and the latter adjusts its voltage gradient accordingly; next there is the period, which the author aptly terms the "transition" period, in which the current and voltage are determined jointly by circuit and arc; in the third period the voltage across the arc is governed principally by the high frequency circuit characteristics and only slightly modified by the arc's conductivity. This is the period to which Mayr's type of equation applies. In my equation, e_a in this period would be varying at a rate comparable with the time-constant of the arc, in which case, of course, the equation would no longer be valid; lastly, there is the period when any arc-type of equation ceases to apply, and the sparkover type of phenomenon becomes appropriate.

In the Paris conference paper, the second of these periods is taken to be the critical one, at any rate for a large circuit breaker working at its limit of breaking capacity. In that period, e_a can be treated as approximately constant and interest is centered around the rising resistance very shortly after zero. It is assumed that, once the arc has gained control, the rate of energy input is no longer raised as a consequence of the *rising resistance* of the arc stream, then, in the following period, e_a will rise sufficiently rapidly to provide for the necessary dielectric strength. The author, on the other hand, regards the third period as being the critical one. I do not think there is sufficient evidence available at the moment to decide which hypothesis is the more correct. It is possible that the correct answer will require a more complete treatment of both periods together. There can be little doubt that, where smaller rms currents are in question, the analysis as applied by the author to the third period, and by Mayr to many other types of arc problem, must be the more appropriate. Deductions like the severity formula must be regarded in the meantime as derived from over-simplifications.

I would point out in conclusion, and here I think the author will agree with me, that the picture of gas blast circuit breaker operations which Mayr puts forward in his paper is not very satisfactory. It appears to be an attempt to translate the convection case into terms of a heat conduction problem, and in so doing overlooks the main factor in extinction by blast. All the experimental evidence goes to show that the extinguishing power of a circuit breaker where blast is not employed in some form or other, with the possible exception of arcs in hydrogen, is relatively insignificant. Any theory of the switchgear arc which fails to take this basic fact into consideration would appear of necessity to be incomplete. There are, none the less, possible alternative views of the function of the blast. A discussion of these, however, is too far removed from the subject of the paper to be in place here.

T. E. Browne, Jr.: In answering the several discussions, I should like to re-emphasize the statement in the introduction that this paper is merely "a preliminary and comparatively elementary exploration of the mathematical approach to dynamic arc behavior" applied particularly to the a-c arc near current zero. It seems proper and is gratifying that a number of questions have been raised about this application. As well expressed by Hoard, this is just a small step in an effort to reduce the art of circuit interrupter design to a science. Very much more needs to be done before a really complete theory usable for circuit breaker design can be evolved. Even the possibility of such a theory is still uncertain, but, as stated by Leeds, it is felt that any clarification of the basic arc theory underlying circuit interrupter design may be of assistance in the design art.

The question raised by Hoover as to the applicability of arc model calculations to a wide range of interrupted voltages and currents and interrupter types must certainly await a great deal more study for an answer. It would appear that any single simplified model would have to be used only in a relatively restricted range of these variables, a rather wide variety of models being required to cover the whole field of design. The particular model presented in the paper has possible utility, as stated, only in the higher ranges of current where appreciable arc conductance is known to persist past current zero.

The caution by Lewis against interpreting general agreement between observed arc current and voltage curves and the model curves as confirming the oversimplified physical pictures of the arc underlying the models serves to reinforce a similar caution expressed in the paper. However, the photographic studies of air-blasted a-c arcs by Kirschstein and Koppelmann[1] and of d-c arcs by Suits[3] do serve to present a picture of the actual arc prior to current zero rather similar to the simplified picture of Cassie's model. Much less is known, of course, about the physical state of the arc space during the fleeting moments at and just after a current zero.

In answer to Lewis' question about the accuracy of location of the zero in the oscillogram of Figure 11*a*, the accuracy is not high but is believed to be sufficient to locate the curves on the rather coarse-scaled plot shown. It is hoped that much more accurate and detailed information about these current transitions will be obtained with an improved amplifier now being built. It is believed that the apparent time displacements observed by Lewis[2] between arc currents and voltages resulted from predictable time lags in his measuring circuits. The interesting but surprising initial fall of the arc resistance observed by Lewis may have been due partly to this time lag but its considerable duration suggests other principal causes. It is interesting to note that for a "square wave" of applied voltage, to which several of Lewis' voltage curves are roughly similar, Figure 6*a* of the paper does show a critical-condition arc resistance, which, though not falling, is constant with respect to time. It is believed that the lack of a unique dielectric strength near critical conditions shown by the multiplicity of curves in Figures 6*b*, 7*b*, and 8*b* does illustrate the observation by Lewis that arc reignition results from energy flow over a period of time, rendering instantaneous dielectric strength of the arc space by any definition of somewhat questionable meaning. However, as pointed out in the paper, the justification for speaking of a definite dielectric strength is the illustrated insensitivity of the arc resistance rise (and dielectric recovery) to restored voltages less than 70 per cent of the critical values coupled with the fact that circuit interrupters normally operate with factors of safety such that α is much less than 70 per cent. Thus, contrary to Skeats' observation, the curves for $\alpha = 0$ are actually of more practical significance than those for the critical restored voltage ($\alpha = 1$).

With most of the other points in the discussion by Skeats, I find myself in general agreement. He is certainly correct in pointing out that the usefulness of the presented simple model is limited by the assumption of no mutual interaction between arc voltage and current. Figures 8*b* and 8*c* for the square law rise do glaringly show this defect, but this idealized voltage recovery case was included because of its possible interest in comparison with the linear rise case of Figure 7 for times somewhat after current zero. The "transition" region very near current zero needs to be treated by some such method as that used by Cassie.

Cassie's discussion is especially appreciated because of my dependence on his earlier work. I am grateful for the further light on his admittedly obscure Paris conference presentation and hope that he will see fit to publish a still more nearly complete account of his calculations in the near future. The segregation of the extinction period into four periods by Cassie does help to clarify the over-all picture, but, as he remarks, practical difficulties arise in considering the transition between these periods which may make necessary combined treatment of some of them. I quite agree with Cassie in his criticism of Mayr's paper (Reference 5 of my paper).

In conclusion, I wish belatedly to call attention to some additional residual arc current records, similar to Lewis' and ours but in a higher power circuit, published in Stockholm in 1946 by Doctor P. Hammarlund.[4]

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A Compressed Air Circuit Breaker for 23-Kv Arc Furnace Duty

H. M. WILCOX
MEMBER AIEE

B. P. BAKER
MEMBER AIEE

THE FIRST SWITCHES for the control of electric circuits combined the functions of switching, protecting and controlling the circuit, in any manner applied. As the range of operating voltage and current expanded, these functions gradually became segregated, the main power circuit breaker assuming the duty of switching and fault protection on high powered circuits while lighter interrupting devices were employed for control of industrial equipment. With the advent of the electric arc furnace, localized intermittent power requirements extended the operating voltage and current range to a point which taxed the ability of industrial control devices and the need of heavier control equipment became evident.

To meet this situation, available oil circuit breakers were drafted into furnace service and applied generally on the high side of the furnace transformers. In addition to their duty controlling the flow of current to the furnace, these circuit breakers were relayed to provide full fault protection for the high voltage furnace feeders. Thus, the circuit breaker had at all times to be in condition to interrupt its full short circuit current rating on a primary fault, ranging up to thousands of amperes, while performing the repetitive duty of a contactor where perhaps 90 per cent of its operations involve the interruption of transformer magnetizing current, generally less than 30 amperes, together with some load current switching involving a few hundred amperes.

Unfortunately, the then available oil circuit breaker possessed an arcing time–current interrupted characteristic which rendered it somewhat unsuited to this application. Its maximum arcing time[1,2] generally coincided with the low range of current at which most of its interrupting operations occurred. The long arcing time on these low current interruptions on a highly inductive circuit resulted in undue amounts of arc energy accompanied by repeated high voltage surges. Since this class of interruption occurred anywhere from 30 to 100 times a day, this resulted in rapid deterioration of oil, and increased maintenance.

As a move toward improving this situation, the air insulated De-ion circuit breaker[3] was introduced into steel mill service in 1932, the first arc furnace application at 13,200 volts being made in 1937. By comparison with previous experience, the performance of these circuit breakers was most gratifying; the oil fire hazard was eliminated, intervals between maintenance periods were doubled and trebled, replacement of operating parts was very materially reduced and interruptions to furnace operation reached a minimum.

A further improvement was obtained by the use of a small cylinder and piston attached to the operating mechanism of the circuit breaker, so arranged as to produce a light blast of air past the parting contacts to assist the magnetic field in forcing the arc into the interrupting chamber. Capacitors, connected from the high voltage leads of the furnace transformer to ground have been used with some success in reducing high voltage surges, although their use appears most effective in reducing surges having their origin in the furnace rather than those associated with switching.[4,5]

Analysis of operating experience up to this time led to the present recommended practice divorcing the primary fault protection function of furnace circuit breakers from the highly repetitive, low current switching operations.[6] This provides a single back-up circuit breaker for protection of the feeders to a battery of furnace transformers, each transformer to have an interrupter specially designed for highly repetitive, low current duty to act as a furnace control but to remain closed under primary fault conditions.

Under this arrangement, the De-ion circuit breaker, being somewhat of a constant current device whose arcing chamber, and consequently the complete circuit breaker structure, depends for size more largely on the voltage to be interrupted rather than the current, was not well adapted to this application. A further trend toward the use of higher voltages in the primary circuit made the use of this circuit breaker still more limited in application, and the field of interrupting devices was canvassed for a more suitable circuit breaker.

The achieved success of the compressed air circuit breaker for power house service[6,7] and its constant arcing time characteristics on low currents, has prompted its development for arc furnace service where reliability, continuity of service and safety are required. These requirements were met by building the circuit breaker, as shown in Figure 1, on a rugged supporting frame with all moving parts mounted on and connected through sealed ball bearings. The contacts and arc splitters, which are the only expendable materials, are accessible for inspection to a man standing on the ground when the circuit breaker is in the open circuit position. These parts are also easily and quickly replaced. All insulating parts are liberally dimensioned for electric integrity and made from various grades of "Micarta" insulation to give a strong, rigid shockproof structure. Surge suppressors, Figure 2, are supplied for use with the circuit breaker, which are introduced in the circuit in parallel with the arc dur-

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H. M. Wilcox is section engineer and B. P. Baker is development engineer, switchgear engineering department, Westinghouse Electric Corporation, East Pittsburgh, Pa.

Figure 1. A 600-ampere 23,000-volt compressed air circuit breaker for highly repetitive electric arc furnace service

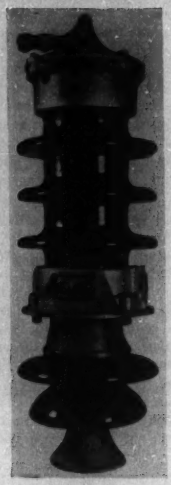


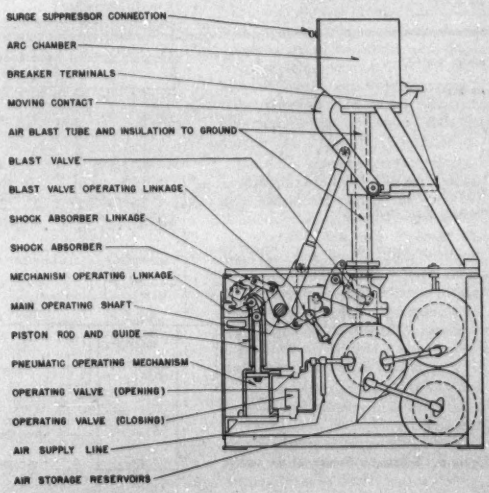
Figure 2. Surge suppressor for use with electric arc furnace circuit breaker

ing the opening and closing operations of the circuit breaker. Their use has reduced the overvoltages to less than one-half of their normal value over the most active current range from 10 to 100 amperes.

The *230-CAF* compressed air circuit breaker shown in Figure 1, has a continuous current rating of 600 amperes with an interrupting rating of 3,000 amperes, and is provided with 34.5-kv class insulation for 23-kv service. It is of the open-frame mounting type and may be located safely in the transformer vault or similar room. As shown in Figures 1 and 3, the circuit breaker frame houses the air storage reservoirs, the pneumatic mechanism, blast valves, control panel and all other parts operating at ground potential. The mechanism consists of a single cylinder and piston, operated by air for both closing and opening. The connecting rod operates the main shaft through two sets of links which form a toggle lock when the circuit breaker is in the closed circuit position. This shaft in turn operates the three moving contacts and the three blast valves. The contacts are connected to the main shaft by wood "Micarta" insulating rods. The blast valves are connected to the main shaft by a simple system of links, which forces the valve open against air pressure during the opening stroke of the circuit breaker, but permits the valves to remain closed during the closing stroke of the circuit breaker. All contacts and blast valves are therefore mechanically tied together and operated in unison by a single pneumatic mechanism. Their adjustment is such that the blast valves open slightly before the contacts separate, but then reclose soon after the contact is withdrawn from the interrupter. To prevent mechanical shock during operation, the moving system is decelerated at both ends of its travel by automobile shock absorbers which are also attached to the main shaft.

All live parts including the contacts, their operating rods, the air blast tube shown in Figure 4, and the interrupter shown in Figure 5 are mounted on top of the frame in open view, although a grill enclosure may be mounted around the circuit breaker when further protection to personnel is desired. The contacts are of the bayonet and finger type. The moving contacts, or bayonet, is hinged at the lower end and rotated back and forth to the open and closed position. Figure 4 shows the moving contact in the almost closed position. When in the open position as shown in Figure 1, the moving contacts are completely withdrawn from the interrupters, so that voltage is removed from the interrupters, leaving the stationary contact and fiber splitters in full view. The interrupter is mounted at the end of the "Micarta" blast tube, adjacent to the stationary contacts, so that the arc is drawn perpendicular to the ends of the splitters which are disposed over the end of the air blast tube. The arc is thus extinguished by the blast of air forcing it against the ends of the fiber splitters.[6] This interrupter is quite different from those used on power house circuit breakers where large values of

Figure 3. Sectional elevation showing the circuit breaker in Figure 1



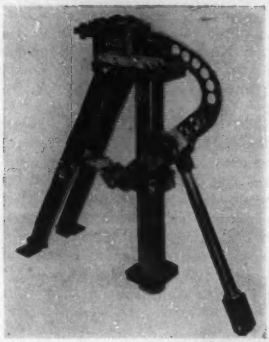


Figure 4. Contact and blast tube assembly, showing the contacts in the almost closed position

Picture taken after 110,000 operations, 30,500 with power

current have to be interrupted. On the power house circuit breakers four of five diverging splitters with intermediate coolers are used. On the furnace circuit breaker eleven simple fiber splitter plates are used. These are mounted parallel and spaced apart without coolers as shown in Figure 5. At the front end of the interrupter is a metal probe extending into almost engaging relation with the moving contact. The surge suppressor is connected to this probe and to the stationary contact. When the circuit breaker is opened, the arc is drawn in the interrupter, and the surge suppressor is connected in parallel with the arc through a very short air gap between the probe and moving contact. The rate of rise of the recovery voltage is thus controlled and the magnitude of the overvoltages limited by the conductivity of the surge suppressor. After the main arc has been extinguished and the moving contact withdrawn from the interrupter, the residual current in the surge suppressor is extinguished between the probe and the moving contact.

The surge suppressor, as shown in Figure 2, consists of a group of lightning-arrester disks, hermetically sealed in a procelain container and mounted on an insulating pedestal. The lightning-arrester disks have a nonlinear resistance characteristic, which permits them to carry large values of current when the applied voltage attempts to go beyond a predetermined value, but for normal crest line voltage the current is limited to a value which is easily interrupted as the moving contact is withdrawn from the interrupter. This characteristic also decreases the amount of energy which would have to be dissipated in the suppressor. The surge suppressor as a complete assembly may be mounted on the circuit breaker or placed at any convenient location.

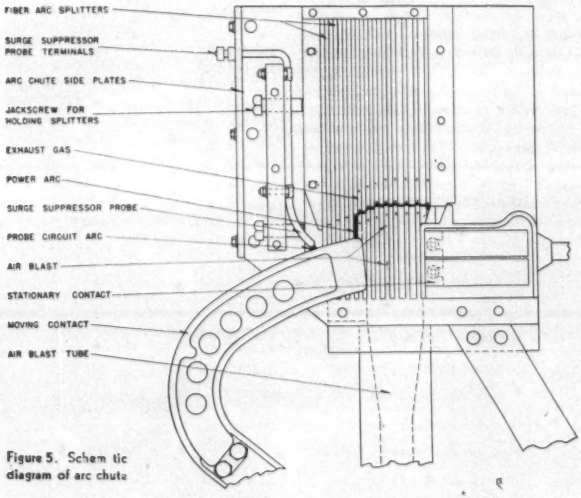


Figure 5. Schem tic diagram of arc chute

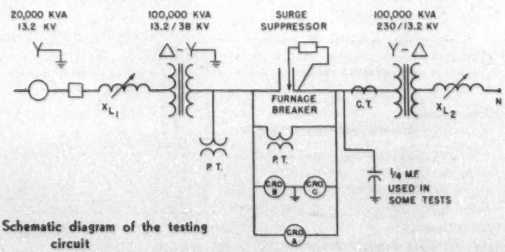


Figure 6. Schematic diagram of the testing circuit

Operating Pressure

The circuit breaker is designed to operate at a pressure of 100 pounds per square inch, but has been demonstrated down to 60 pounds per square inch. This permits the use of the shop air supply, which however, is recommended for emergencies only. Normally, a separate air supply should be provided, which may be adequate for several circuit breakers, depending upon the service requirements. The air supply unit consists essentially of an electric motor, a 2-stage air compressor, and storage reservoirs, in which air is stored at 250 pounds per square inch, there it is cooled, and expanded to the circuit breaker operating pressure of 100 pounds per square inch which leaves it clean and dry.

Test Circuit

Figure 6 is a schematic diagram of the test circuit which was set up in the laboratory to check the operation of the circuit breaker under conditions approximating those found in service. One and some times two 20,000-kva 3-phase 13.2 kv Y-connected grounded neutral generators were used as the source of power. Following the generator circuit breaker was a 3-phase bank of air-core reactors to control the current. From there power was supplied to the high side of a bank of 3-phase 100,000-kva transformers connected 13.2 kv to 38 kv. The low side of the transformer bank was connected in delta, and the high side wye with a grounded neutral for most of the tests. This gave a rather stiff source of variable power similar to that which may be found in

service. On the load side of the test circuit breaker was another 3-phase 100,000-kva bank of transformers, connected 230 kv to 13.2 kv. The high side which was next to the test circuit breaker was wye-connected and ungrounded for most of the tests. The low side was in delta. One-fourth-microfarad capacitors were provided for grounding the load terminals of the circuit breaker on some of the tests. The load beyond the transformer consisted of another bank of 3-phase air-core reactors. In some respects, this type of load is quite different from an arc furnace. However, the switching surges are found to be just as severe. Current and potential transformers for use on recording devices were located as shown. Cathode ray oscillograph connections were made as shown to obtain a record of the voltage surges from line to ground, load to ground and across the circuit breaker. By varying X_{L1}, X_{L3}, the transformer ratio on both banks and the generator excitation, a wide range of current and voltages could be obtained.

Test Results

The circuit breaker was given over 110,000 operations, 30,500 of which were with power. Generally the power tests were interspersed with groups of tests without power. Most of the tests were on a 3-phase basis with 23 kv line-to-line. Current values from 0.3 to 200 amperes were obtained by keeping X_{L1} constant at 5 ohms and varying X_{L3} from infinity to zero. With X_{L1} constant at 0 ohms and X_{L3} varying from infinity to 0.073 ohm, currents up to 1,200 amperes were obtained with one generator as shown in Figure 6. With two generators 1,600 amperes were obtained. By increasing the excitation of the generators to give $34^1/_2$ kv at the circuit breaker terminals currents up to 2,250 amperes were obtained. For currents around 3,000 amperes, the circuit-breaker terminals were short-circuited before going into the transformer, and the generator excitation adjusted to give a test voltage of 27.6 kv. For 3,500 amperes, the excitation was raised to 36 kv.

The tests performed were distributed over the current range generally as they may appear in service. About 500 operations were between 1,400 and 3,500 amperes; 3,000 between 300 and 500 amperes, and the remainder around 20 amperes where the overvoltages seem to be the greatest. Figure 7A is a curve showing the surge voltage across the test circuit breaker plotted as a function of current interrupted for a circuit as shown in Figure 6. The dots represent the tests without any voltage-suppressing means. The circles represent the tests in which the surge suppressor was used. The numbers to the right of the points indicate the number of times during that test that the voltage reached 75 per cent or more of the voltage indicated by the point; namely, at 30 amperes, 23 kv, without the surge suppressor, the voltage reached a maximum of 105 kv and three additional times it came to 79 kv or more. This represents 5.6 times normal line-to-ground voltage which is in good agreement with published steel mill data.[8] Figure 7B is a similar curve made under the same circuit conditions as Figure 7A, except the load terminals of the circuit breaker were connected to ground through a 1/4-microfarad capacitor. From these curves, it appears that the surge suppressor may be relied upon to reduce the overvoltages to approximately 40 per cent of their free value.

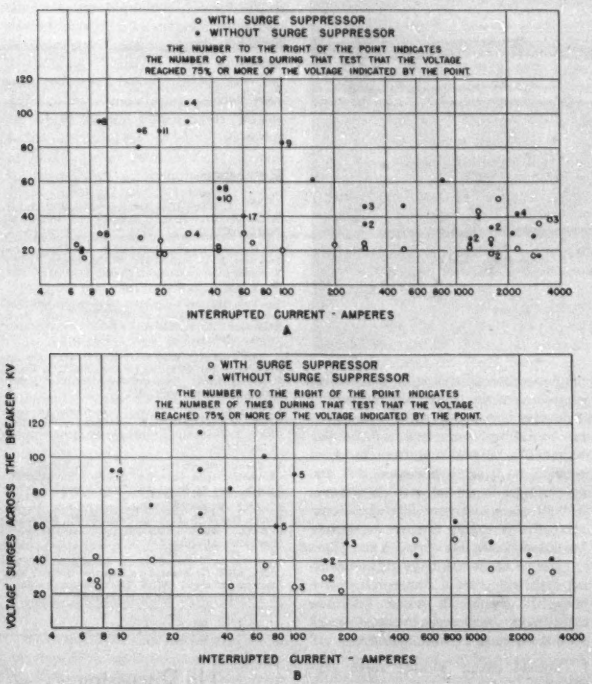
Since this type of interrupter develops interrupting ability as the arc is drawn across additional splitters, the arcing time or arc length is an indication of interrupting effort. It has been observed that the arcing time bears a direct relation to the magnitude of the voltage surges. The arcing time has a maximum between 10 and 100 amperes for the free oscillating circuit. When the surge suppressor is used, the arcing time is quite uniform and seldom exceeds one cycle.

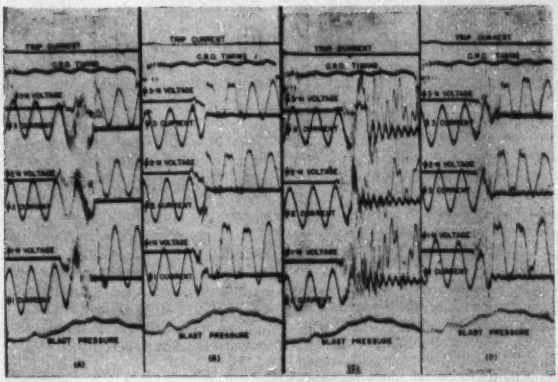
The use of the grounding capacitor on the load lead has very little effect on the overvoltages across the circuit breaker except to slightly shift the range of current where maximum overvoltage occurs to higher current values. If the use of the ca-

Figure 7

A. Plot, showing the magnitude of voltage surges across the circuit breaker as a function of current interrupted, tested on a freely oscillating circuit. With and without the surge suppressor

B. Plot, showing the same operating characteristics as *A* except the load terminals were grounded through a $^1/_4$-microfarad capacitor





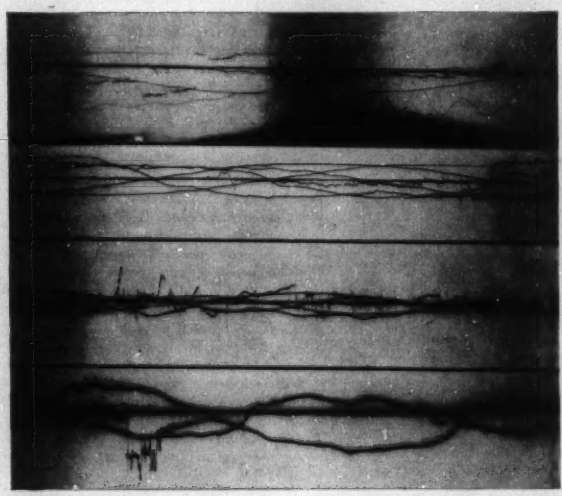


Figure 8. Magnetic and cathode ray oscillogram of typical operations of the circuit breaker on the circuit shown in Figure 6 at 24.6 kv, 3 phase, 15 amperes

A. Normal circuit without surge suppressor
B. Normal circuit with surge suppressor
C. Circuit with $^1/_4$-microfarad grounding capacitor without surge suppressor
D. Circuit with $^1/_4$-microfarad grounding capacitor with surge suppressor

pacitor moves this maximum overvoltage range away from the most active current range, an apparent operating improvement would be observed. Removing the ground from the power bank of transformers and grounding the load bank showed no noticeable difference in the voltage surges across the test circuit breaker.

Several tests made with the cathode ray oscillograph connected from the line terminal to ground and from the load terminal to ground, indicated that the overvoltages to ground were about equal in both cases, and were only about one-half of the voltage across the circuit breaker as shown in Figures 7A and 7B.

Figure 8 shows the magnetic and cathode-ray oscillogram of four representative tests. It should be noted that the cathode-ray oscilloscope voltage scales are not the same for all four tests.

1. Normal circuit without surge suppressor.
2. Normal circuit with surge suppressor.
3. With grounding capacitor and without surge suppressor.
4. With grounding capacitor with surge suppressor.

All four tests were made on the circuit shown in Figure 6, 24.7 kv at 15 amperes.

Conclusion

A compressed-air circuit breaker has been developed for highly repetitive duty in electric arc furnace service. An intensive series of tests has demonstrated that with its strong, shock-resistant construction and with all parts designed for repetitive duty, this circuit breaker is capable of withstanding many thousands of operations in the current ranges of magnetizing, load and secondary fault currents, when operated at normal shop air pressure. A life test of 30,000 interruptions in these current ranges has been made on this circuit breaker without repairs, replacement of contacts or any other operating element.

The use of a nonlinear resistance surge suppressor across the circuit-breaker terminals during arcing, is recommended to reduce surge voltages to less than 40 per cent of their normal free value which may reach six times line-to-ground voltage in the current range from 10 to 100 amperes.

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No Discussion

Transient Shaft Torques in Turbine Generators Produced by Transmission-Line Reclosing

J. W. BATCHELOR
MEMBER AIEE

D. L. WHITEHEAD
ASSOCIATE AIEE

J. S. WILLIAMS
ASSOCIATE AIEE

EVALUATION OF TORQUES which develop in turbine generators during circuit reclosing or synchronizing operations deserves detailed analysis, even though in existing installations and under normally contemplated conditions it is unlikely that many machines will be mechanically stressed beyond recommended limits. The integrated procedure described here is primarily intended to supply a quantitive check on those future installations which, because of generally lower machine and system impedances, may prove more critical than those now in service. High-speed reclosing tie lines are the most frequently encountered system components which allow circuit reclosing while phase displacement exists, but other components or situations may be responsible for three pole synchronizing out of phase, and will also be subject to the analysis given here.

In addition to the detailed methods applicable to the final analysis of a critical case, a basis for estimating the severity of transient torques should prove valuable for identifying those situations which will require complete solution.

System losses and decrements are important in their effect on the magnitude of peak transient torques, and no solution of a critical case should be considered complete without modifications based on system resistances and decrements within the machine.

Electrical Torque In No Loss Circuits

To evaluate the total mechanical torque developed in a turbine generator shaft during 3-phase synchronizing, it is first necessary to solve the electrical system for the torque developed between rotor and stator of the generator. This torque is basically dependent upon the interaction of currents and magnetic fields within the machine,[1] but may be more conveniently expressed in terms of the machine internal voltage, machine impedance, system impedance, and angular displacement between system and machine voltage at the instant of reclosure.[2] Figure 1*a* is an equivalent circuit, showing one phase of a circuit which contains the essential quantities to be dealt with in a no-loss no-decrement circuit. Development of an equation which represents the variation of electrical torque immediately following circuit closure, as is outlined in Appendix II, indicates that two components combine to form resultant transient electrical torque. One is a constant torque produced by rotating field flux and alternating armature current, and the other is an alternating fundamental frequency torque produced by rotating field flux and the direct component of initial armature current. These two torque components combine in proportions which are determined by the angle of reclosure. A unique feature of the problem is the angle at which these two torque components combine to form a unidirectional torque peak. If the machine voltage is advanced with reference to system voltage prior to reclosing, a maximum torque impulse tending to retard the machine will occur when the reclosing angle is 120 degrees. Figure 2 is a sketch of the transient electrical torque throughout two cycles, based on reclosing angles of 120 and 240 degrees. The peak unidirectional torque will vary in magnitude along with a change in the reclosing angle, as indicated in Figure 3. It is important to note that circuit reclosure must occur simultaneously on the three phases if this analysis is to be valid.[3]

Electrical Torques in a Circuit Which Contains Loss and Decrement

Sources of power loss and current decrement that will always exist in an actual circuit, and almost always affect electrical torque to a degree worthy of consideration, are listed here:

1. Armature resistance of generator.
2. Circuit resistance external to generator.
3. Decrement of direct components of current in armature circuit.
4. Decrement of subtransient components of current in armature.
5. Decrement of internal voltage of machine.
6. Losses within the machine which produce "loss torques."

In the derivation outlined in Appendix III, the foregoing modifying factors have been applied to the basic equation of the previous section in a manner which must admit to some approximation, but which is believed to be comparable in accuracy with other working methods widely used in synchronous machine analysis.

One outstanding factor remains, however, to limit the applicability of the results shown here to the first one or two cycles immediately following circuit re-

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J. W. Batchelor, design engineer, a-c generator engineering department, D. L. Whitehead, engineer, central station engineering department, J. S. Williams, application engineer, industry engineering, central station section; are all with Westinghouse Electric Corporation, East Pittsburgh, Pa.

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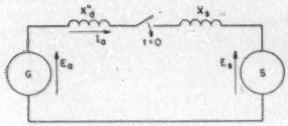


(a)

$$T=\frac{|E_a|^2}{X''_d+X_s}\sin\delta\left(1-\cos\omega t+\tan\frac{\delta}{2}\sin\omega t\right)$$

(b)

Figure 1

(a) Basic equivalent circuit which represents 3-phase synchronizing of machine with a system. (b) Equation for transient electrical torque in machine

closure. It is the reduction in angular displacement between machine and system which will proceed following circuit reclosure. So long as maximum torques are to be derived, conditions which develop after the first two cycles are of secondary importance.

Solution of Electrical Torques by Network Calculator

Seldom will a synchronizing problem involve electrical circuits that can be readily reduced to an equivalent single mesh network. To provide for the probability that complex networks will appear in connection with torque problems, and to combine torque and system stability problems whenever the system involves a high-speed reclosing tie, a calculating method applicable to the a-c network calculator has been devised.

If a power system is set up for study on an a-c network calculator, the change in machine armature currents produced by any circuit reclosing operation may be measured for various combinations of system loading and interconnection. Since subtransient currents determine peak torque values, the transient values of machine impedance normally used during stability studies should be replaced by subtransient values, but only at the instants of time which immediately precede and follow the reclosing operation. To obtain a guide to the application of loss and decrement factors, the equivalent system impedance external to the generator after reclosure should be measured with system voltages removed. With these quantities determined, a solution for transient electrical torque may be established according to the derivation in Appendix IV. Load torque which exists on a machine just prior to a reclosing operation tends to retard the generator, and should be added in proper sense to transient mechanical torque in order to obtain total shaft torque.

Application of the Electrical Analog Computer to the Determination of Transient Shaft Torques Produced by Synchronizing Out of Phase

The use of the electrical analog computer in the solution of linear differential equations is well known.[6] Essentially the solution consists of the determination of an electrical network whose differential equations for voltage and current are analogous to the equations to be solved. Boundary conditions are imposed on the input function to the network analogy as dictated by the original differential equations that are to be solved. The solutions are obtained in the form of direct readings or pictures from an oscilloscope which is connected by means of metering buses to points in the analogy, thereby permitting a direct reading of any of the desired unknowns.

The problem to be solved in the case of synchronizing out of phase is the determination of the maximum torque which will be developed in the shaft of a turbine-generator unit which is synchronized out of phase over the range of 0 to 180 degrees.

The torque developed in the rotor during synchronizing takes the form

$$T=Ae^{-\lambda t}-Be^{-\alpha t}\cos\omega t+Ce^{-\gamma t}\sin\omega t \quad (1)$$

where A, B, and C are functions of δ where δ is the angle of synchronizing; λ, α, and γ are decrement factors. This torque acts on the mechanical system consisting of the generator rotor, with an inertia constant I_1, the generator to turbine shaft having a spring constant K, and the turbine with an inertia constant I_2.

The equation for dynamic equilibrium of the mechanical system is given by

$$T=I_1\frac{d^2\theta_1}{dt^2}+K(\theta_1-\theta_2) \quad (2)$$

and

$$O=I_2\frac{d^2\theta_2}{dt^2}+K(\theta_2-\theta_1) \quad (3)$$

where

T = input torque to the generator rotor
I_1 = inertia constant of the generator rotor
I_2 = inertia constant of the turbine rotor
θ_1 = angular displacement of the generator rotor relative to synchronous position
θ_2 = angular displacement of the turbine rotor relative to synchronous position
K = spring constant of shaft connecting the generator and turbine

If a mass-inductance analogy is used, voltage will correspond to torque, current to velocity, and charge to displacement. A simple circuit which will produce voltage components corresponding to the terms of the torque equation 1 is shown in Figure 4.

The electrical analogy for the mechanical system is shown in Figure 5. The correspondence between the electrical network of Figure 5 and the mechanical system can be readily seen by writing the equations for dynamic equilibrium in

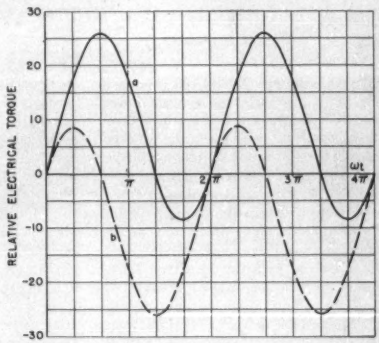


Figure 2. Variation of per unit electrical torque following circuit reclosing

Reclosing angles are (a) 120 degrees and (b) 240 degrees, with $E_a^2/(X_d''+X_s)$ equal to 10

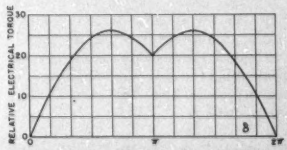


Figure 3. Magnitude of crest per unit electrical torque

Developed by reclosing angles between 0 and 360 degrees, with $E_a^2/(X_d''+X_s)$ equal to 10

terms of voltage and charge for the network.

$$e = L_1\frac{d^2q_1}{dt^2} + \frac{1}{c}(q_1 - q_2) \tag{4}$$

$$O = L_2\frac{d^2q_2}{dt^2} + \frac{1}{c}(q_2 - q_1) \tag{5}$$

Note the similarity between equations 2 and 4, and 3 and 5. The second term in equations 4 and 5 represents the voltage drop across the capacitor, and since voltage is analogous to torque, gives directly the torque developed in the shaft.

For general studies it is advantageous to reduce the 2-mesh circuit of Figure 5 to an equivalent single-mesh circuit. Rewriting equations 4 and 5 in terms of the currents i_1 and i_2 and the differential operator, p, we have

$$e = L_1 p i_1 + \frac{1}{cp}(i_1 - i_2) \tag{6}$$

$$O = L_2 p i_2 + \frac{1}{cp}(i_2 - i_1) \tag{7}$$

Multiplying equation 7 by $\frac{L_1}{L_2}$ we have

$$O = L_1 p i_2 - \frac{L_1}{L_2}\,\frac{(i_1 - i_2)}{cp} \tag{8}$$

Thus

$$e = L_1 p(i_1 - i_2) + \left(\frac{L_1 + L_2}{L_2}\right)\left(\frac{i_1 - i_2}{cp}\right)$$

Equation 9 suggests a single-mesh circuit as shown in Figure 3, where $L_0 = L_1$

$$C_0 = C\left(\frac{L_1 + L_2}{L_2}\right) \tag{10}$$

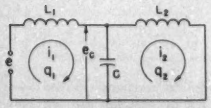


Figure 5. Electrical analogy for turbine generator

$$i_0 = i_1 - i_2 \tag{11}$$

and e_{c0} is analogous to T_o and e_c is analogous to T, giving a ratio between T and T_o of

$$\frac{T}{T_o} = \frac{L_2}{L_1 + L_2} \tag{12}$$

The natural frequency of the system is given by

$$f = \frac{1}{2\pi}\sqrt{\frac{L_1 + L_2}{L_1 L_2 C}} \text{ cycles per second} \tag{13}$$

This permits us to reduce the shaft torque as a function of frequency to a general curve independent of the turbine inertia. In Figure 7 are curves showing the crest transient torques developed in the shaft for various angles of synchronization as determined on the electrical analog computer.

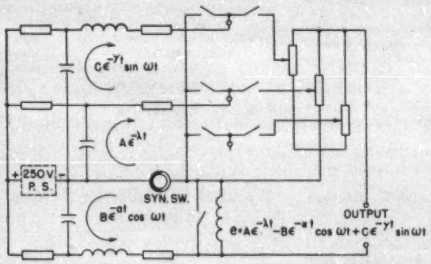


Figure 4. Forcing function for air gap torque analogy

Maximum Mechanical Torque Considerations

American standards require that a generator "shall be capable of withstanding, without injury, the stresses of a 10-second, 3-phase short circuit at its terminals when operating at rated kilovolt-amperes, power factor, and 5 per cent overvoltage, or any other 10-second short circuit provided the machine phase currents under the fault condition are limited by means of suitable reactance or resistance to a value which does not exceed the maximum phase current obtained from the 3-phase fault."[7] This requirement serves to define the capability of the shaft with respect to the peak torque which may be applied, and may be used as the criterion for the maximum torque to be developed in the case of out-of-phase synchronizing, since no specific standard has been defined for that particular case. It has been shown[8] that the most severe shaft torque for normal design turbine generators with a mechanical system frequency of the order of 30 cycles per second is developed in the case of a line-to-line fault at the generator terminals with the machine operating at 105 per cent voltage and full load. The torque under this condition, then, is that for which the shaft is designed, and that which should not be exceeded under any other operating condition. A fault at the terminals of the generator is relatively improbable, and the out-of-phase synchronizing condition is one which may occur relatively frequently on systems where 3-phase reclosing is used. Therefore, the maximum allowable torque which should be developed under the latter condition should be somewhat less than the maximum allowable torque for which the machine was designed, in order that the unit may not be repeatedly subjected to the most severe shocks allowable. A figure of 75 per cent of the torque obtained on a line-to-line fault at the full load and 105 per cent voltage has been used in this study for the maximum allowable torque for the out-of-phase synchronizing condition.

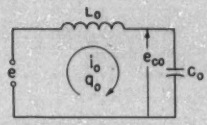


Figure 6. Equivalent single-mesh circuit for turbine generator analogy

Figure 8 shows the relation of shaft torque and synchronizing angle for a typical 30,000-kw 85-per cent power factor, 3600-rpm 60-cycle turbine generator with a 2-mass mechanical system. For this same unit, the shaft torque for a line-to-line fault at full load and 105 per cent voltage is 4.35 times normal. Thus, for the condition just referred to, the maximum allowable torque would be 3.25 times normal, and the maximum allowable synchronizing angle may be determined from Figure 8 for various values of system reactance.

While the shaft torque reaches a maximum for a synchronizing angle of approximately 120 degrees, the forces on the stator winding reach a maximum at a synchronizing angle of 180 degrees. As in the case of the maximum allowable shaft torque, the forces on the stator winding resulting from the relatively frequent out-of-phase synchronizing should be limited to some value less than that for which the machine was designed, and for this limit a value of 75 per cent of the forces caused by a 3-phase fault at the generator terminals with the machine operating at full load and 105 per cent voltage has been

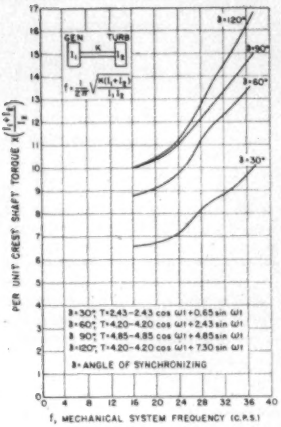


Figure 7. Crest shaft torque for typical 30,000-kw turbine generator

$X_s = X_d = 0.109$ per unit
$E_a = E_s = 1.03$ per unit

used. Figure 9 shows the allowable limits of system reactance and synchronizing angle for maximum forces on the shaft and stator windings for the 30,000-kw generator referred to previously. Conditions which lie within the cross-hatched area are unsafe.

Probability of Developing Excessive Torques by Transmission Line Reclosing

Most applications of high-speed tie line reclosing inherently involve factors which will limit the repetitive torques to values below recommended limits. Relatively high decrements associated with transmission circuits and the presence of other transmission lines effectively paralleling the reclosing tie line will act to reduce electrical torques.

If a paralleling tie line should be out of service at a time when the reclosing sequence proceeds, stresses greater than normally anticipated will occur in connected machines. Lightning storms may affect an area so that line outage and reclosing will occur coincidentally on adjacent transmission lines.

Several methods have been considered to reduce the possibility of such undesirable results of simultaneous faults, and among the most practicable is single pole tie line reclosing. This plan arranges for only the faulted phase of a transmission line to be opened and reclosed automatically, leaving unfaulted phases available to keep the system stable and prevent the development of a large reclosing angle. If a fault involving more than one phase develops, automatic reclosing may be prevented by lock-out devices.

Appendix I. Nomenclature

X_d'' = subtransient reactance of machine
X_d' = transient reactance of machine
X_2 = negative sequence reactance of machine
X_s = reactance of system external to machine
R_a = d-c resistance of machine armature circuit
R_s = resistance of circuit external to machine
R_1 = positive sequence resistance of machine
R_2 = negative sequence resistance of machine
E_a = phase to neutral vector voltage behind machine subtransient reactance, in per unit based on rated machine voltage
E_s = phase to neutral vector voltage of reference phase system voltage, expressed in per unit based on rated machine voltage
e_a = instantaneous value of E_a (maximum $e_a = \sqrt{2}\,|E_a|$)
e_s = instantaneous value of E_s (maximum $e_s = \sqrt{2}\,|E_s|$)
I_a = current in machine armature circuit, expressed in per unit based on machine rated current
i_a = instantaneous value of I_a (maximum $i_a = \sqrt{2}\,|I_a|$)
T_d'' = time constant of subtransient component of a-c armature current
F = decrement factor applicable to short circuit alternating currents in machine armature
F' = decrement factor applicable to synchronizing alternating currents in machine armature
F'' = decrement factor applicable to synchronizing alternating currents in machine armature, similar to F' except derived from a-c network calculator information
A = decrement factor applicable to direct currents in machine armature
δ = angle by which machine internal voltage leads system voltage at moment of synchronizing
T = electrical torque developed between rotor and stator of generator, in per unit based on machine output at rated voltage, rated current, and unity power factor. The torque is expressed as a positive quantity when it acts in a direction to retard the machine's rotation
T_L = loss torque produced in machine during reclosing
θ = power factor angle of complete circuit, including machine and system impedances
ϕ = reactive factor angle of complete circuit ($\phi = 90 - \theta$)
β = angle between internal machine voltage and armature current (The angle is negative when current lags the internal machine voltage)

Note: All impedances are expressed in per unit on the machine kilovolt-ampere base.

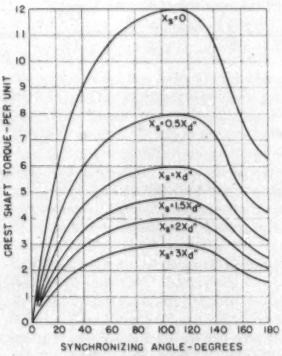


Figure 8. Crest shaft torque of 30,000-kw turbine generator as a function of synchronizing angle and system reactance

Three phase synchronizing. Mechanical frequency 23.3 cycles per second
$I_1 = 8{,}800$ lb in. sec^2, $I_2 = 10{,}500$ lb in. sec^2

Appendix II. Three-Phase Electrical Synchronizing Torque in Basic Circuit

Making the two assumptions that losses and decrements are negligible and that the machine and system voltages are equal in magnitude, the following derivation is based upon Figures 1*a* and 10, and provides an expression for electrical torque in the synchronized machine.[3]

$$\delta = 2\alpha \tag{14}$$

$$E_a = |E_a|\,\epsilon^{j\alpha} \tag{15}$$

$$E_s = |E_a|\,\epsilon^{-j\alpha} \tag{16}$$

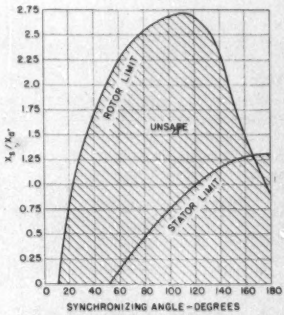


Figure 9. System reactance required with 30,000-kw turbine generator for safe synchronizing, as a function of synchronizing angle

Three-phase synchronizing

$$E_a - E_s = j2|E_a| \sin \alpha \tag{17}$$

$$I_a = \frac{E_a - E_s}{j(X_d'' + X_s)} = \frac{2|E_a| \sin \alpha}{X_d'' + X_s} \tag{18}$$

Real components of the machine internal voltages follow from equation 15.

$$e_a = \sqrt{2}|E_a| \cos (\omega t + \alpha) \tag{19}$$

$$e_b = \sqrt{2}|E_a| \cos (\omega t + \alpha + 240) \tag{20}$$

$$e_c = \sqrt{2}|E_a| \cos (\omega t + \alpha + 120) \tag{21}$$

Real components of machine armature currents, including d-c terms, follow from equation 18.

$$i_a = \frac{2\sqrt{2}|E_a| \sin \alpha}{X_d'' + X_s} [\cos \omega t - \cos 0] \tag{22}$$

$$i_b = \frac{2\sqrt{2}|E_a| \sin \alpha}{X_d'' + X_s} [\cos (\omega t + 240) - \cos 240] \tag{23}$$

$$i_c = \frac{2\sqrt{2}|E_a| \sin \alpha}{X_d'' + X_s} [\cos(\omega t + 120) - \cos 120] \tag{24}$$

$$T = {}^1/_3(e_a i_a + e_b i_b + e_c i_c) \tag{25}$$

$$= \frac{|E_a|^2 \sin \delta}{X_d'' + X_s}\left[1 - \cos \omega t + \tan \frac{\delta}{2} \sin \omega t\right] \tag{26}$$

$$= \frac{|E_a|^2}{X_d'' + X_s}\left[\sin \delta - 2 \sin \frac{\delta}{2} \cos \left(\omega t + \frac{\delta}{2}\right)\right] \tag{27}$$

Appendix III. Derivation of Transient Electric Torque Considering Losses and Decrements

This section is based on the circuit of Figure 11, the plan being first to determine electrical torque caused by that portion of system voltage which is equal in magnitude to machine voltage, and then to superpose the effect of the remaining component of system voltage.

$$T = T' + T'' + T_L \tag{28}$$

$$Z = R_1 + R_s + j(X_d'' + X_s) \tag{29}$$

$$\theta = \tan^{-1}\left(\frac{X_d'' + X_s}{R_1 + R_s}\right) \tag{30}$$

$$\phi = 90 - \theta \tag{31}$$

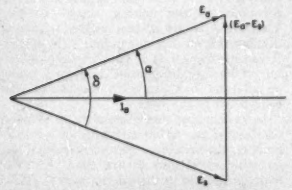


Figure 10. Vector diagram associated with the basic equivalent circuit shown in Figure 1a

Applying a field decrement factor to the real component of machine internal voltage:

$$e_a = \sqrt{2}|E_a| F' \cos (\omega t + \alpha) \tag{32}$$

Introducing direct current components and applying appropriate decrement factors:

$$i_a' = \frac{2\sqrt{2}|E_a| \sin \alpha}{Z} [F' \cos (\omega t + \phi) - A \cos \phi] \tag{33}$$

$$T' = \frac{2|E_a|^2}{Z} \sin \frac{\delta}{2} \cos \left(\frac{\delta}{2} + \phi\right)\left[F'^2 - F'A \cos \omega t + F'A \tan \left(\frac{\delta}{2} + \phi\right) \sin \omega t\right] \tag{34}$$

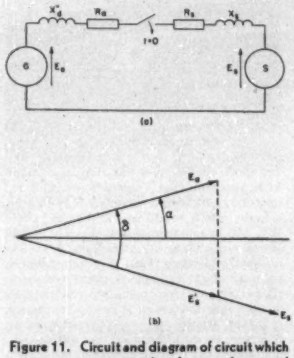


Figure 11. Circuit and diagram of circuit which contains resistance and voltages of unequal magnitude

The torque component which must be combined with T' in the event that internal machine voltage does not equal system voltage is derived below, assuming that E_s exceeds E_s'.

$$E_d = E_s - E_s' = |E_d| e^{-j\alpha} \tag{35}$$

$$T'' = \frac{|E_d||E_a|}{Z_1} \sin (\delta - \phi)[F''^2 - F''A \cos \omega t + F''A \cot (\delta - \phi) \sin \omega t] \tag{36}$$

If E_s' should exceed E_s, the sign of T'' will be reversed.

Loss torques in the machine act always to retard the rotor, hence their algebraic sign will be positive according to the assumptions of this paper. To approximate the loss torque, the condition of synchronizing two identical machines at a 180 degree closing angle is considered. This is equivalent to the 3-phase fault condition, for which loss torques are known. Since the loss torques are dependent upon internal machine voltage, a factor of reduction for this voltage which is a function of the reclosing angle seems to be appropriate.

$$T_L = \sqrt{2}\left[\frac{\sqrt{2}A}{(X_d'' + X_s)}|E_a| \sin \frac{\delta}{2}\right]^{1.8} (R_2 - R_1) + \left[\frac{2F'}{X_d'' + X_s}|Ea| \sin \frac{\delta}{2}\right]^2 \left(R_1 + \frac{R_s}{2}\right) \tag{37}$$

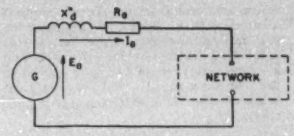


Figure 12. Circuit representative of reclosing operations which occur within a complex system network

Appendix IV. Derivation of Transient Electrical Torque From Information Furnished by A-C Network Calculator

The following quantities, identified in Figure 12, should be measured at the machine:

E_a = Voltage behind subtransient reactance, determined during a stability study by replacing all machine transient reactances with subtransient reactances, and readjusting internal voltages to maintain identical system conditions. These changes are to be made at the instant immediately before a circuit reclosing operation occurs within the network

ΔI_a = Change in machine current caused by a switching operation within the network, measured in complex form relative to E_a

Z_s = Impedance of network after circuit reclosure has been accomplished, measured with all network voltages zero

$$e_a = \sqrt{2}F'' |E_a| \cos \omega t \tag{38}$$

$$\Delta i_a = \sqrt{2} |\Delta I_a| [F'' \cos (\omega t + \beta) - A \cos \beta] \tag{39}$$

$$T = |E_a| |\Delta I_a| [F''^2 \cos \beta - F''A \times \cos (\omega t - \beta)] \tag{40}$$

To approximate decrement factors and loss torques, β and $\delta/2$ may be considered of equal magnitude.

Appendix V. Decrement Factors

Decrement of direct current components in the armature is the more important of the two decrements considered here:

$$A = \epsilon^{-t[2\pi f(R_a + R_s)(X_a + X_s)]} \tag{41}$$

Decrement of the subtransient quantities (a-c armature current and voltage behind subtransient reactance) within the machine is complicated by several factors not present in short circuit problems. First, the system contains a machine internal voltage which will decay, and a system voltage which has been considered constant. Second, the current which circulates within the armature is not always completely effective in demagnetizing the main field of the machine because of its large quadrature axis component at small reclosing angles. One approach to a solution is to assume that the machine is synchronized with a system

whose characteristics are in all respects identical to the machine's. At a synchronizing angle of 180 degrees, then, the currents and voltages in the machine will appear as if a 3-phase short circuit had been applied to the machine terminals, and previously developed subtransient decrement factors will apply.[2,3,5]

$$F = \frac{X_d''}{X_d'} + \frac{X_d''(X_d' - X_d'')}{X_d''X_d'}\,\epsilon^{-\frac{t}{\tau_d''}} \qquad (42)$$

The demagnetizing effect of armature current will be produced by current in the direct-axis of the machine. The direct-axis component of armature current varies as $\sin \delta/2$, so a modified field decrement is:

$$F' = \frac{X_d''}{X_d'} + \frac{X_d''(X_d' - X_d'')}{X_d''X_d'} \times \left[1 - \left(1 - \epsilon^{-\frac{t}{\tau_d''}}\right) \sin \frac{\delta}{2}\right] \qquad (43)$$

When the actual system characteristics depart markedly from those of the machine, a factor replacing $\sin(\delta/2)$ can be determined by calculating the actual ratio of direct axis armature current to total armature current.

In calculating T'', a different form of equation 43 should be used:

$$F'' = \frac{X_d''}{X_d'} + \frac{X_d''(X_d' - X_d'')}{X_d''X_d'} \times \left[1 + \left(1 - \epsilon^{-\frac{t}{\tau_d''}}\right) \sin \beta\right] \qquad (44)$$

References

1. Definition of an Ideal Synchronous Machine and Formula for the Armature Flux Linkages, **R. H. Park.** *General Electric Review* (Schenectady, N. Y.), volume 31, 1928, pages 332–34.

2. Transient Electrical Torques of Turbine Generators During Short Circuits and Synchronizing, **H. S. Kirschbaum.** AIEE Transactions, volume 64, 1945, pages 65–70.

3. Transient Torques in Synchronous Machines, **L. A. Kilgore, M. Stone.** AIEE Transactions, volume 52, 1933, pages 945–52.

4. Determination of Short Circuit Torques in Turbine Generators by Test, **E. C. Whitney, H. E. Criner.** AIEE Transactions, volume 59, 1940, pages 885–9.

5. Determination of Transient Shaft Torques in Turbine Generators by Means of the Electrical-Mechanical Analogy, **G. D. McCann, C. E. Warren, H. E. Criner.** AIEE Transactions, volume 64, 1945, February section, pages 51–6.

6. Synchronous Machines, Three-Phase Short Circuit of Synchronous Machines—Part V, **R. E. Doherty, C. A. Nickle.** AIEE Transactions, volume 49, 1930, pages 700–14.

7. Rotating Electrical Machinery. American Standards Association, **C50-1943,** (New York, N. Y.), page 26, paragraph 3.130.

Discussion

A. W. Rankin (General Electric Company, Schenectady, N. Y.): The subject paper has presented an analysis of the shaft torques in turbine-generators during reclosing operations, and has made several statements concerning the "allowable limits of system reactance" during such reclosing operations.

In discussing allowable limits of system reactance and the importance of the concomitant shaft torques, some details are needed concerning shaft stresses since the stress is the quantity which must be limited, and system reactance is important only in its relation to the shaft stresses. The authors state that the American Standards on short circuits "serves to define the capability of the shaft with respect to the peak torque which may be applied." The writer agrees with this statement only in particular designs. The shear stress at the journals and adjacent shaft sections is the largest shear stress present except for concentrations, and these concentrations are of importance only in fatigue, which is not a major factor here. Yet this shear stress at the journals is quite small in comparison to the centrifugal stresses which are present throughout the rotor body, and an appreciable multiplying factor due to torsional resonance is needed before this shear stress gets dangerously high. In general, therefore, it is not a correct statement to say as the authors do that "The torque under this condition, then, is that for which the shaft is designed. . . . .". The shaft is designed for the centrifugal stresses in the rotor body, and only when the design is very close to torsional resonance must the shaft be designed around the torsional stresses.

In the particular example chosen by the authors, the writer would estimate that the nominal journal shear is of the order of 5,000 pounds per square inch, and with a multiplying factor of 3.25 as given in the paper, the journal shear stress is still below the centrifugal stresses in the rotor body, and this is true even when the shear stresses are converted to their equivalent tensile stresses. It should be noted that these centrifugal stresses are present whenever the machine is at rated speed whereas the shear stresses discussed here are present only during short circuits and reclosing operations. It is also true that there are several other operating conditions, such as simply clearing a fault, which would in some cases exceed this 3.25 times normal torque. The writer would appreciate it, therefore, if in the authors' reply they would give some numerical data about the actual shear stresses so that a quantitative evaluation can be made of the suggested limits of system reactance.

If the writer has been presumptuous, however, in assuming that the authors are making actual recommendations on system reactances, there is presented in the following a discussion of the paper on the basis of the explicit material contained therein. The study itself is very interesting, and illustrates the practical results which can be obtained on the electric-analog and similar computers. It is interesting to observe the degree by which mechanical computers are affecting design and operating engineering, and it is expected that when high-speed digital computers reach a reasonable cost that they will revolutionize our present engineering methods.

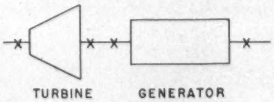


Figure 1. Single-cylinder turbine set

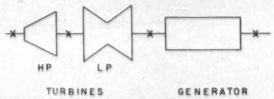


Figure 2. Tandem-compound, double-flow turbine set

Figure 3. Tandem-compound, triple-flow turbine set

Returning to the subject paper, it is disappointing that the authors did not study the reclosing shaft torques on a variety of machine designs rather than the single example which was chosen. The single-cylinder two-body design as presented in this paper and as shown in Figure 1 of this discussion is one of the simplest that is built by the Company with which this writer is associated. Furthermore, the machine chosen for an example in the subject paper is one which was relatively far from torsional resonance with the system.

As examples of other designs, the writer suggests the tandem-compound double-flow sets. These must be designed with considerable care to ensure that the torsional frequencies do not get too close to resonance with the system frequency. A diagram of such a 3-body system is shown in Figure 2 of this discussion. Each of the tandem-compound double-flow units built by the company with which this writer is associated is carefully analyzed to locate its torsional frequencies, and to keep such frequencies from resonance with the electrical system.

As an example of a design even more complex than the tandem-compound double-flow sets, the writer suggests the tandem-compound triple-flow set which was recently analyzed by the writer and his associates for torsional stresses during short circuits. This was a 5-body system such as is shown in Figure 3 of this discussion. The shear stresses in the shaft of this set were determined on our differential analyzer. Such an analyzer was necessary because of the complexity of the system.

In concluding this discussion on system reactances, the writer recommends a *single* limit on the minimum system reactance for reclosing operations rather than the two limits suggested in the subject paper. This single limit should be simply that the system reactance must be equal to or greater than the transient reactance of the machine.

Such a system reactance would limit the stator-winding stresses during reclosing to values less than those present during the ten-second short circuit defined in the standards. It should then be the responsibility of the manufacturer to ensure that the concomitant shaft torques would not result in dangerous shaft stresses. By Figure 8 of the subject paper, the crest shaft torque for the example chosen would then be about five times normal, and, if the writer's estimate of the nominal shear stress is reasonably close, the resulting crest shear stress is not dangerous from shaft stress considerations.

The writer believes that a single limit as recommended in this discussion would be a far more satisfactory arrangement than the double standard suggested in the subject paper.

The writer would also like to point out a discrepancy in the published data on the relative severity of a line-line fault from full load and a reclosing operation at 90 degrees out of phase for a system reactance equal to the generator subtransient reactance. Figure 7 of a paper by McCann, Warren, and Criner[1] indicates that the line-line fault from full load produces more severe shaft torques than the reclosing operation, for the conditions stated previously, for a 2-body system, especially so for a set with a low torsional frequency.

The subject paper presents the opposite conclusion, however, and states that the line-line fault from full load, for the example chosen by the authors, will have a crest torque of 4.35 times normal, but that reclosing the same machine 90 degrees out of phase with a system reactance equal to the generator subtransient reactance will produce a crest torque of about six times normal.

REFERENCE

1. See reference 5 of the paper.

M. D. Ross (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The major emphasis in the paper is upon the possible damage to the generator due to excessive shaft torques. While the shaft may be considerably twisted due to bad out-of-phase synchronizing which upsets the balance of the unit, the damage to the stator may also be considerable. As few engineers ever see the damage to stator windings, I have picked out a typical illustration from the files as a matter of interest. Figure 4 of the discussion shows the damage to a 25,000-kva 1,800-rpm generator built about 20 years ago and would be typical of damage due to

Figure 4

excess current, whether from a short circuit or synchronizing out of phase. A superficial inspection of the picture shows nothing unusual but a close check will show the distorted coils at the junction between phase groups. In a case like this, it would be necessary to completely rewind the stator in order to put it in good shape.

Faulty synchronizing of machines happens very rarely and does not justify building into the generator the ability to stand such faulty operations without damage to the shaft or the windings. The cost of the generator would be greatly increased if it were found to be possible to make such a design. In such a case as we are considering here, where we deliberately synchronize out of phase, it is therefore important to carefully consider the stresses in the generator set up by such operation. This paper is a step in the direction of setting up limits which will safeguard the generator and the authors are to be congratulated on their very constructive paper.

J. W. Batchelor: It is possible that Rankin has misinterpreted the intent of the paper, which was to discuss shaft stresses produced by particular transients in the electrical system. In this case, the effect of centrifugal stress is not being considered. Centrifugal stresses and transient torsional stresses under fault conditions do not bear a fixed relationship to each other, since their separate magnitudes may be affected by various factors in the design of the unit. The figures in the paper apply to the particular unit which was under study, and do not necessarily apply to other units which must be designed to meet a wide variety of conditions.

The use of a single limit of external reactance equal to or greater than the transient reactance of the generator would provide ample margin in the large majority of cases of normal machines of present day proportions connected through reclosing tie-lines to high voltage transmission systems.

The reference to the apparent discrepancy between the subject paper and Figure 7 of the paper by McCann, Warren, and Criner[1] calls attention to the importance of the proper determination of system conditions at the moment of reclosure. The curves in the earlier paper were drawn for radically different machine proportions, as well as for different voltage and load conditions. Consequently, exact agreement is not to be expected.

Ross' comments and illustration demonstrate a problem which was only touched upon in the paper, but which is of interest to those who have system operating conditions requiring transmission line reclosing.

REFERENCE

1. See reference 5 of the paper.

Arcing Fault Currents in Low-Voltage A-C Circuits

C. F. WAGNER
FELLOW AIEE

L. L. FOUNTAIN
MEMBER AIEE

THE recommended AIEE method[1] of calculating short-circuit currents in circuits of 600 volts and below is based upon the assumption of a "bolted" fault. Circuit breakers applied to such circuits are rated on the maximum value of the average rms currents in the three phases. The effect of dissymmetry is included by multiplying the symmetrical short-circuit current for the bolted-fault condition by the factor 1.25 which then gives the average current in the three phases. This corresponds to a short circuit in which maximum dissymmetry occurs in one phase, and the X/R ratio of the circuit is 11.8. The rms current in the phase of maximum dissymmetry is 1.48 times the rms symmetrical current. Some engineers believe that the fault current calculated upon the basis of a bolted fault is inordinately high and that such values are not obtained in practice. Whether this be true or not is dependent to a large extent upon the nature of the fault, upon how the faults are initiated, and upon the effect of the fault arc in limiting the short-circuit current.

In 1941, O. R. Schurig[2] presented data on the effect of fault voltage drops and impedances upon the short-circuit currents in low-voltage circuits. In his synopsis, the statement appears that "Instances are shown where actual fault currents reach 100 per cent of the rms symmetrical values calculated for bolted faults." In his conclusions, the following statement appears "The current-limiting effect of accidental arc faults in low-voltage bus structures may be of a minor order, arc fault currents as high as 88 and 100 per cent (average 3-phase values) of the rms symmetrical bolted-fault short-circuit current having been obtained in tests at 300 volts and 600 volts line-to-line, open-circuit voltage, respectively." These figures represent a reduction from the bolted-fault factor of 1.25 to an arcing fault factor of 0.88 or 1.0. Schurig in a footnote recognizes this effect as representing a reduction to 70 and 80 per cent for 300 and 600 volt circuits respectively as compared to a bolted fault.

Without attempting to specify the conditions under which various types of electrical apparatus should be applied to low voltage circuits, there appears to be a distinct need to clarify the situation insofar as the effect of arc drop is concerned. An investigation of this subject is in progress by the working group on calculating short circuit currents in low-voltage systems of AIEE switchgear committee. As a contribution to this activity, the Westinghouse Electric Corporation undertook the tests described in this paper. These tests were made in standard metal-enclosed bus structures, the short circuit being initiated by fusing copper wires and by metal objects resting across the bus conductors. Various sizes of fuse wire were used to determine what effect this might have upon the current. Naturally, as the size of the wire is increased, bolted-fault conditions are approached.

Description of Test

Figures 1*a* to 1*d* are illustrations of the conventional cubicle in which the faults were initiated. Standard bus construction and supports were used. The vertical 3-phase bus run in Figure 1*a* consists of $1/4 \times 4$ inch copper bars spaced four inches and the horizontal run $1/4 \times 6$ inch copper bars also spaced four inches. Figure 2 is a schematic diagram of the circuit used. Power was supplied from a 3-phase source through cables. The open circuit voltage of the circuits was adjusted to 500, 250, and 125 volts. As determined from the bolted-fault tests, the X/R ratio of the circuit was 11.8.

The circuit was adjusted approximately to give a "nominal" value of current. For each "nominal" setting, tests were made with a bolted fault. The "bolted fault" was made by clamping copper straps across the busses at the same point at which the fuse wire was connected. Figure 1*a* shows the fuse wire in position. Figure 1*a* also shows the metal bar used in certain tests to initiate the fault as it lay in position across the horizontal bus run. An oscillogram was taken of each short circuit.

Test Results

As is characteristic of arcs, the drop across it was found to be quite variable. The oscillogram of Figure 3*a* (Test 2-72956) is typical of this condition. As the arc drop is an appreciable fraction of the circuit voltage, the variations in the arc drop are reflected in the magnitude of the current, a large arc drop tending to produce a small current and a small arc drop, a large current. The arc in Figure 3*a* was initiated by a number 14 copper wire connection from phases 1 to 2 and phases 2 to 3. Figure 3*b* is an oscillogram of the short-circuit current for the same condition except that the fault is a bolted one.

To determine the first half-cycle values of current for the arcing fault, the peak current of the first significant half cycle in each of the three phases was measured. It was then assumed that these half cycles of current were of sinusoidal wave shape. These values and the average rms of the currents in the three phases are tabulated in Table I for the 500-volt circuit; and in

Paper 48-26, recommended by the AIEE industrial power systems; switchgear; and transmission and distribution committees and approved by the AIEE technical program committee for presentation at the AIEE winter general meeting, Pittsburgh, Pa., January 26–30, 1948. Manuscript submitted November 14, 1947; made available for printing, December 22, 1947.

C. F. WAGNER is manager, central station engineering department and L. L. FOUNTAIN is an electrical engineer, with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

Figure 1A

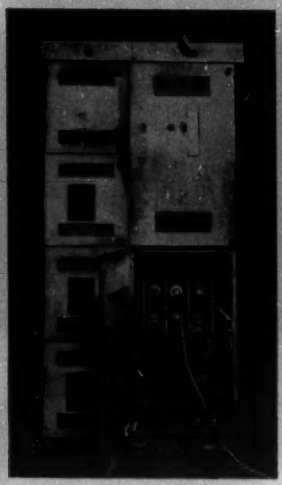


Figure 1C

Figure 1D

Figure 1. Conventional metal-clad switchgear cubicle used in tests

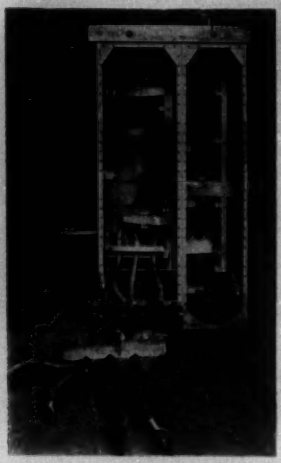


Figure 1B

Table II, for the 250-volt circuit. Two groups of tests were made several months apart. The division between the groups is indicated by the gap in the tables. In the tabulation of the single-phase tests the rms currents were expressed both in terms of the 3-phase bolted symmetrical current and the single-phase bolted symmetrical current. In comparing the arcing currents with the single-phase bolted symmetrical currents, it must be remembered that for a bolted single-phase short circuit the maximum rms current for a circuit whose X/R is 11.8 is

Figure 2. Schematic diagram of test circuit

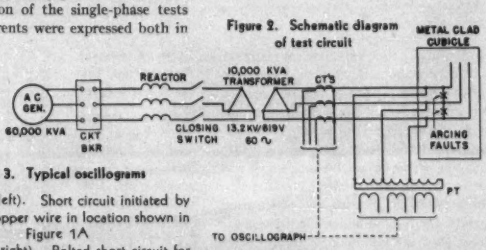
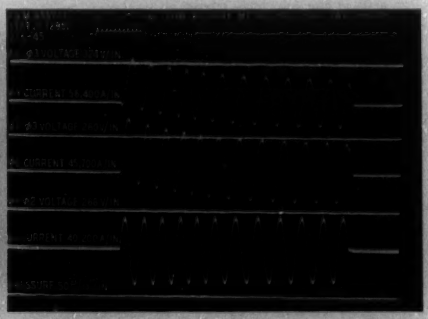
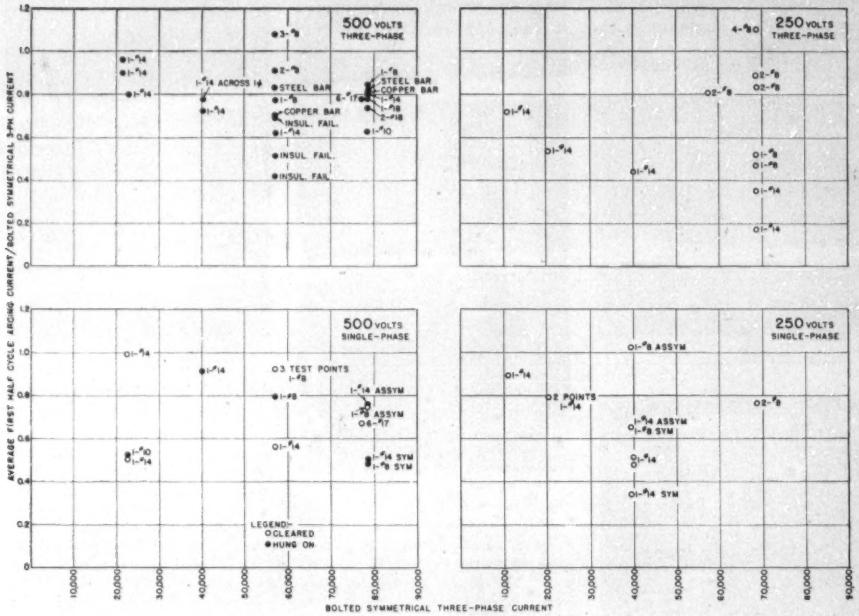


Figure 3. Typical oscillograms

(a) (below, left). Short circuit initiated by number 14 copper wire in location shown in Figure 1A

(b) (below, right). Bolted short circuit for same circuit condition as (a)





1.48 times the symmetrical current.

The average first half-cycle rms currents are also plotted in Figure 4 against the average bolted symmetrical rms current. In many of the tests, the arc extinguished and cleared the circuit before tripping of the circuit breaker which was set for a time of 10 cycles. To differentiate between the clearing and nonclearing points, the tests in which the arc was extinguished are indicated by open circles and those in which the arc hung on are indicated by blacked circles. All the 3-phase tests at 500 volts hung on and all the 250-volt tests both single-phase and 3-phase cleared without the circuit breaker opening. The results with single-phase 500 volts were mixed.

For a given condition of initiation, the ratio of the first half-cycle arcing current to the bolted-fault current generally decreases as the bolted-fault current is increased.

A large number of points were taken with the circuit impedances set to give 68,000 amperes and 78,600 amperes for the symmetrical bolted condition at 500 volts and to give 58,000 amperes at 250 volts. Different sizes of copper wire were used to initiate the faults. In Figure 5, the

Figure 4 (above). Average first half-cycle rms currents

Notations indicate size of copper wire used as fuse and other methods of initiating the fault. Open circles indicate that short circuit was self clearing

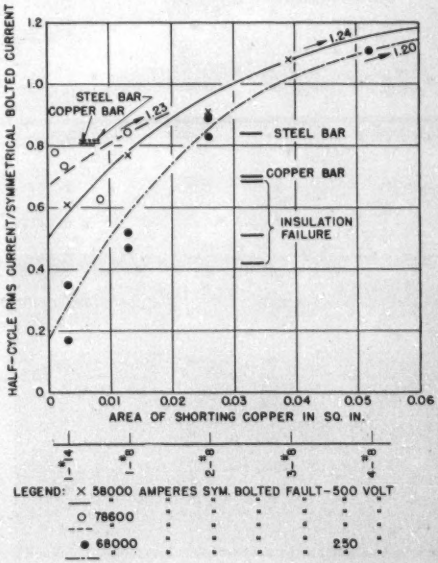
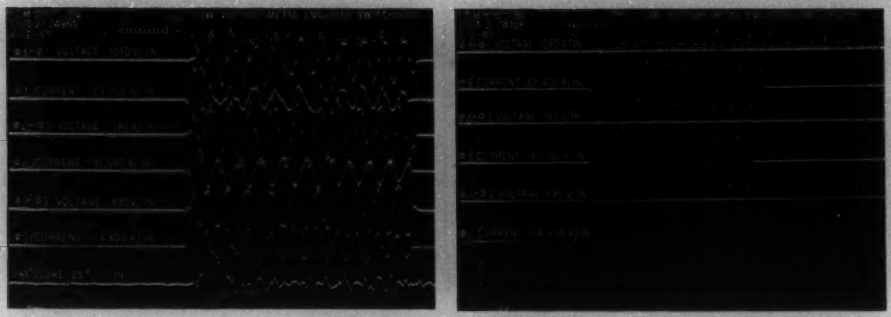


Figure 5. First half-cycle rms current as affected by nature of initiating means



(A) (B)

Figure 6. Oscillograms of 3-phase short circuit initiated by three number 8 copper wires connected across each of two pairs of busses at location shown in Figure 1A

Initial bolted symmetrical current is 57,100 amperes. Low voltage following initiation of short circuit denotes fusing period

results of the tests for these three conditions are plotted against the area of the shorting copper. The figures with the arrows indicate the magnitudes of the currents for the bolted-fault condition corresponding to the respective circuits. With very large wire, the fusing time becomes so long that bolted conditions are approached. As evidence of this condition, see Figure 6 (Test 2-74965). The low values of

Table I. Initial Half-Cycle Values of Short Circuit Current—500 Volts—60 Cycle

Test No.	Phases	Fuse Wire	Initial Bolted Sym. Current Rms	First Half Cycle Rms Arcing Currents ϕ_1	ϕ_2	ϕ_3	Average	Arcing Rms I / Bolted Sym.
2-72956	3	1—No. 14	21,400	16,100	24,900	16,800	19,300	0.90
2-72957	3	1—No. 14	21,400	23,300	25,800	12,750	20,620	0.965
2-72969	3	1—No. 14	40,000	24,100	36,000	27,200	29,100	0.72
2-72967	3	1—No. 14	40,000	31,900	33,600	28,300	31,300	0.775
2-73029	3	6—No. 17 twisted	77,200	62,900	69,800	49,700	60,800	0.79
2-74761	3	1—No. 14	57,100	28,400	40,900	35,300	34,900	0.61
2-74762	3	1—No. 8	57,100	51,100	51,900	47,400	50,100	0.77
2-74964	3	2—No. 8	57,100	36,400	60,800	59,100	52,100	0.91
2-74965	3	3—No. 8	57,100	48,600	75,700	60,900	61,700	1.08
2-74966	3	Steel bar (1/4×2×8)	57,100	36,500	57,800	47,900	47,400	0.83
2-74967	3	Copper bar (1/4×1 1/8×10)	57,100	34,800	42,900	42,600	40,100	0.70
2-74763	3	Insulation failure	57,100	22,700	27,900	21,300	24,000	0.42
2-74764	3	Insulation failure	57,100	42,100	44,900	30,800	39,300	0.69
2-74765	3	Insulation failure	57,100	32,400	31,900	23,700	29,300	0.51
2-83101	3	1—No. 18	78,600	72,100	61,900	50,100	61,400	0.78
2-83102	3	2—No. 18	78,600	67,800	60,600	44,900	57,800	0.735
2-83103	3	1—No. 10	78,600	65,100	40,000	43,800	49,500	0.63
2-83104	3	1—No. 8	78,600	76,400	65,300	58,400	66,400	0.845
2-83105	3	Copper bar (1/4×1 1/8×10)	78,600	60,600	58,100	73,000	63,900	0.81
2-83121	3	Steel bar (1/4×2×8)	78,600	75,000	59,400	58,400	64,300	0.82
2-83098	3	1—No. 14	22,600	11,900	20,400	21,700	18,000	0.80

Test No.	Phases	Fuse Wire	Initial Bolted Sym. Current For Same Circuit: Three-Phase	Single-Phase	Symmetrical or Asymmetrical	First Half-Cycle Rms Arcing Current	Arcing Rms I / 3-Phase Bolted Sym. I	Arcing Rms I / Single-Phase Bolted Sym. I	Remarks
2-72968	1	1—No. 14	40,000	35,800	Asymmetrical	36,500	0.91	1.02	
2-73028	1	6—No. 17 twisted	77,200	67,700	Asymmetrical	51,800	0.67	0.765	Only 1 1/2 cycle duration.
2-74760	1	1—No. 14	57,100	50,800	Asymmetrical	31,900	0.56	0.63	Only 1 cycle duration.
2-74963	1	1—No. 8	57,100	50,800	Asymmetrical	44,900	0.79	0.88	
2-74968	1	1—No. 8	57,100	50,800	Asymmetrical	52,700	0.92	1.04	Only 1 cycle duration.
2-74969	1	1—No. 8	57,100	50,800	Asymmetrical	52,700	0.92	1.04	Only 1/2 cycle duration.
2-74970	1	1—No. 8	57,100	50,800	Asymmetrical	52,700	0.92	1.04	Only 1/2 cycle duration.
2-83097	1	1—No. 14	22,600	21,400	Asymmetrical	22,455	0.99	1.05	Only 1 cycle duration.
2-83099	1	1—No. 14	22,600	21,400	Asymmetrical	11,200	0.50	0.52	Only 1/4 cycle duration.
2-83100	1	1—No. 10	22,600	21,400	Asymmetrical	11,700	0.52	0.55	
2-83126	1	1—No. 14	78,600	66,800	Asymmetrical	59,000	0.75	0.88	
2-83127	1	1—No. 8	78,600	66,800	Asymmetrical	58,700	0.75	0.88	1 cycle of arcing with several restrikes of small magnitude.
2-83128	1	1—No. 14	78,600	66,800	Symmetrical	39,400	0.50	0.59	After 2 cycle, periods of zero current of 1/8 cycle duration occurred.
2-83129	1	1—No. 8	78,600	66,800	Symmetrical	37,700	0.48	0.56	

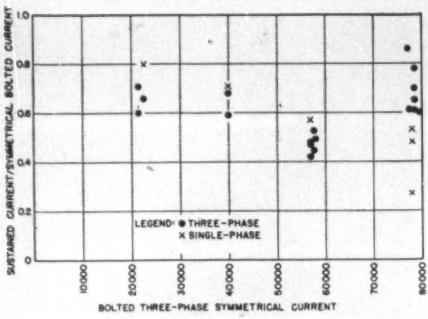


Figure 7. Sustained current averaged over third to sixth cycles—500-volts circuit

voltage immediately after application of the voltage to the circuit indicates the time required for the development of the arc.

Likewise, in Figures 4 and 5 are plotted the first half-cycle values of current when the fault is initiated by a copper or steel bar resting across the busses. In Figure 5, these values are indicated by short heavy horizontal lines whose relations to the abscissa have no significance. In all cases, the bars were thrown from their positions violently. These values lie intermediate between the values obtained for small and for large wire. After a large number of tests, the interior of the structure and the insulators became covered with a finely divided conducting deposit. Some evidence of this coating may be seen in Figure 1*a* in which a portion had been rubbed off by the operator in connecting the fuse wires. The points in Figures 4 and 5 marked "insulator failure" represent flashovers of the insulator resulting from this deposit.

As stated previously, the arcing faults on the 250-volt tests were not sustained but cleared themselves. After the first two cycles on the 500-volt circuit, the transient effects associated with the initiating process appear to have disappeared. The currents thereafter are dependent upon the impedance of the circuit and the arc drop. To determine the nature of these effects, the peak currents were measured for both halves of the third to sixth cycles, inclusive. These values were then averaged and converted to rms values. The results are tabulated in Tables III and IV for the 3-phase and single-phase short circuits, respectively, and plotted in Figure 7. Both the 3-phase and the line-to-line arcing currents are plotted against the 3-phase bolted-fault currents. Only one value, and this 0.86, exceeded 0.8.

As another approach in determining the effect of the arc on limiting the current, the arc voltage was measured for each half cycle between the third and sixth cycles. The results of these measurements are

Table II. Initial Half-Cycle Values of Short-Circuit Current—250 Volts—60 Cycles

Test No.	Phases	Fuse Wire	Initial Bolted Sym. Current Rms	First Half Cycle Rms Arcing Current ϕ_1	ϕ_2	ϕ_3	Average	Arcing I / Bolted Sym.	Remarks
250 Volts									
2-72958	3	1—No. 14	10,870	4,800	10,200	8,600	7,870	0.72	Phase 3 interrupted in 1/2 cycle. Maximum value during fault is 1 phase.
2-72970	3	1—No. 14	20,433	9,100	13,600	10,100	10,930	0.54	Only 1 cycle duration.
2-73027	3	1—No. 14	40,220	21,100	12,400	19,800	17,800	0.44	Only 1 cycle duration.
2-74641	3	2—No. 8	57,200	31,400	54,900	53,300	46,500	0.81	Arced for 1 cycle; cleared 1 cycle; restruck for 1 cycle
2-74747	3	1—No. 14	68,400	13,800	19,900	11,800	11,900	0.17	Only 1/4 cycle duration.
2-74748	3	1—No. 14	68,400	27,600	15,000	29,600	24,100	0.35	Only 1 cycle duration.
2-74638	3	1—No. 8	68,400	23,800	37,900	35,500	32,400	0.47	Only 1 cycle duration.
2-74749	3	1—No. 8	68,400	31,400	39,900	35,500	35,600	0.52	Arced for 1 cycle; cleared and restruck on 5th cycle.
2-74639	3	2—No. 8	68,400	60,300	51,900	56,900	56,700	0.83	Arced for 1 1/2 cycle; cleared and restruck on 3rd cycle.
2-74750	3	2—No. 8	68,400	60,300	59,900	61,500	60,600	0.89	Only 1 1/2 cycle duration.
2-74751	3	4—No. 8	68,400	69,000	74,800	83,000	75,600	1.11	Only 1 cycle duration.
125 Volts									
2-74752	3	1—No. 8	(34,200)	18,000	24,800	28,200	23,700	0.69	Only 1/2 cycle duration.

Test No.	Phases	Fuse Wire	Initial Bolted Sym. Current For Same Circuit: Three-Phase	Single-Phase	Symmetrical or Asymmetrical	First Half-Cycle Rms Arcing Current	Arcing Rms I / 3-Phase Bolted Sym. I	Arcing Rms I / Single-Phase Bolted Sym. I	Remarks
2-72954	1	1—No. 14	10,870	9,850	Asymmetrical	10,750	0.99	1.09	Only 1/2 cycle duration.
2-72965	1	1—No. 14	20,400	17,800	Asymmetrical	16,100	0.79	0.90	Only 1/2 cycle duration. Phase 3 dead
2-72966	1	1—No. 14	20,400	17,800	Asymmetrical	16,100	0.79	0.90	Only 1/2 cycle duration. Phase 3 hot—Did not strike
2-73025	1	1—No. 14	40,200	35,000	Asymmetrical	19,400	0.48	0.55	Only 1/2 cycle duration. Phase 3 dead
2-73026	1	1—No. 14	40,200	35,000	Asymmetrical	20,600	0.51	0.59	Only 1/2 cycle duration. Phase 3 hot—Only slight spit
2-74640	1	2—No. 8	68,400	62,300	Asymmetrical	51,800	0.79	0.83	Only 1/2 cycle duration.
2-83124	1	1—No. 14	(39,300)	34,100	Asymmetrical	25,600	0.65	0.75	Only 1/2 cycle duration.
2-83125	1	1—No. 8	(39,300)	34,100	Asymmetrical	40,000	1.02	1.17	Only 1 1/2 cycle duration.
2-83122	1	1—No. 14	(39,300)	34,100	Symmetrical	13,200	0.34	0.39	Only 1/2 cycle duration.
2-83123	1	1—No. 8	(39,300)	34,100	Symmetrical	25,600	0.65	0.75	Only 1 cycle duration.

Note: Values in parentheses obtained by application of higher voltage to same circuit.

Table III. Short-Circuit Data Obtained From Third to Sixth Cycles—500 Volts, 3-Phase—60 Cycle

Test No.	Fuse Wire	Initial Bolted Sym. Current	Average Current in 3, 4, 5 and 6th Cycles Crest			Rms			Average Rms	Average in 3 to 6	Peak Voltages Phase-to-Phase 3, 4, 5 and 6th Cycles				Average Rms Arc Voltage		
			ϕ_1	ϕ_2	ϕ_3	ϕ_1	ϕ_2	ϕ_3		Init. Bolt. Sym. I	2-3	1-3	1-2	Aver.	Phase to Phase	Phase to Neutral	Fraction of Line Volt.
2-72956	1—No. 14	21,400	18,500	20,600	15,800	13,100	14,500	11,200	12,900	0.6	428	325	380	378	268	154	0.53
2-72957	1—No. 14	21,400	21,700	22,000	20,300	15,400	15,500	14,400	15,100	0.71	466	359	354	393	278	160	0.55
2-72969	1—No. 14	40,000	37,500	30,000	32,100	26,500	21,200	22,700	23,500	0.59	432	316	430	393	278	160	0.55
2-72967	1—No. 14	40,000	34,580	41,000	29,500	24,400	29,000	27,000	27,100	0.68	410	324	398	377	264	152	0.53
2-73029	6—No. 17 twisted	77,200	87,300	83,300	68,800	29,300	29,900	23,700	27,300	0.48	403	437	420	421	298	172	0.60
2-74761	1—No. 14	57,100	44,600	36,700	33,500	31,500	25,900	23,700	27,000	0.47	405	406	411	407	288	166	0.58
2-74762	1—No. 8	57,100	44,600	36,700	33,500	31,500	25,900	23,700	27,000	0.47	405	406	411	407	288	166	0.58
2-74961	3—No. 8	57,100	33,800	40,800	28,300	23,500	28,800	20,000	24,100	0.42	465	515	380	420	297	171	0.60
2-74966	Steel bar (1/2×2×8) resting across bar	57,100	37,800	45,200	24,600	26,700	32,000	17,400	25,400	0.445	437	482	367	429	303	175	0.61
2-74967	Copper bar (1/4×1/2×10)	57,100	46,800	45,000	35,300	33,100	31,800	25,000	30,000	0.525	459	464	398	440	311	180	0.62
2-74764	Insulation Failure	57,100	44,700	35,200	35,200	31,000	27,900	24,900	28,100	0.49	410	410	452	424	300	173	0.60
2-83102	2—No. 18	78,607	89,250	98,000	70,800	63,119	69,300	50,707	61,000	0.78	411	184	323	306	216	125	0.43
2-83102	2—No. 18	78,600	72,165	74,156	57,900	51,000	52,400	40,900	48,100	0.612	413	219	359	330	234	135	0.47
2-83103	No. 10	78,600	71,900	65,900	79,300	50,800	45,600	56,200	51,184	0.652	417	230	428	358	244	141	0.51
2-83104	No. 8	78,600	64,800	69,100	67,300	45,800	45,900	47,900	46,900	0.597	420	275	417	365	274	158	0.55
2-83105	Copper bar 1/4×1/2×10	78,600	77,200	89,700	78,600	54,600	57,000	54,800	55,800	0.698	402	277	380	353	250	144	0.50
2-83121	Steel bar 1/4×2×8	78,600	69,810	73,600	74,900	47,200	49,300	48,500	48,400	0.616	437	379	377	398	281	162	0.56
2-83098	1—No. 14	22,600	22,500	21,800	19,000	15,900	15,400	13,500	14,900	0.66	436		423	430	303	175	0.61

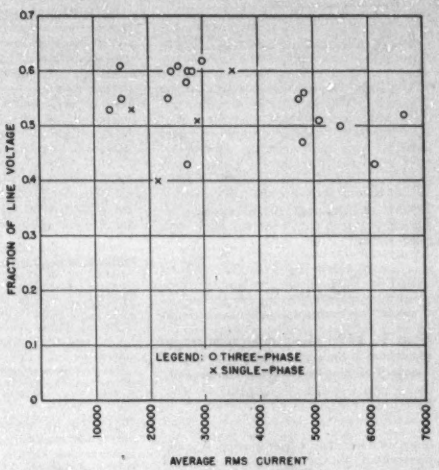


Figure 8. Arc voltage averaged over three phases—500-volt circuit

likewise tabulated in Tables III and IV and plotted in Figure 8. These values lie in general between 0.4 and 0.6 of the line voltage which in this case was 500 volts.

As a means of reflecting the effect of the arc drop upon the average rms current in the three phases, the curves in Figure 9 were computed. The abscissa expresses the arc drop as a per cent of the line voltage. The dotted lines and their continuation into the full lines give the symmetrical component of the current as a fraction of the symmetrical component of current with no arc (bolted fault) for three assumed ratios of reactance to resistance of the metallic portion of the circuit. The small circles on the dotted lines indicate the time constant of the circuit including the arc drop as a resistance drop. These in turn provide the medium to determine the extent by which the currents should be increased to include asymmetry. Since the arc drops for Figure 8 lie between 0.4 and 0.6 of the circuit voltage, the curves of Figure 9 for this range indicate that for the arcing fault condition, the average rms currents should lie between about 0.7 and 0.9 of the symmetrical current assuming that the fault developed into an arcing fault instantly.

Summary

1. On 250-volt and 125-volt a-c circuits, arcing faults are unstable and will extinguish themselves within two cycles or less even when initiated by four number 8 copper wires on a bus with 4-inch spacing. On

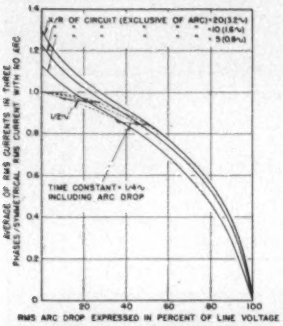


Figure 9. Effect of arc drop upon average rms current in three phases at end of first half cycle (including asymmetrical component)

Dotted line gives symmetrical component only.

Table IV. Short-Circuit Data Obtained From Third to Sixth Cycles—500 Volts, Single Phase—60 Cycles

Test No.	Fuse Wire	Symmetrical or Unsymmetrical	Initial Bolted Symmetrical Current: Single-Phase	Initial Bolted Symmetrical Current: Three-Phase	Aver. Rms Current in 3, 4, 5 and 6th Cycles	Arcing Rms I 3φ Sym. I	Arcing Rms I 1φ Sym I	Aver. Rms Arc Voltage for 3, 4, 5 and 6th Cycles: Volts	Aver. Rms Arc Voltage for 3, 4, 5 and 6th Cycles: Fraction of Applied Volt
2-72968	1—No. 14		35,800	40,000	21,600	0.54	0.71	202	0.40
2-74963	1—No. 8		50,800	57,100	28,900	0.51	0.57	257	0.51
2-83100	1—No. 10		21,400	22,600	17,100	0.76	0.80	268	0.53
2-83126	1—No. 14	Asymmetrical	66,800	78,600	35,300	0.45	0.53	296	0.60
2-83121	1—No. 14	Symmetrical	66,800	78,600	18,200	0.23	0.27		
2-83129	1—No. 8	Symmetrical	66,800	78,600	32,150	0.41	0.48		

Note: Nine other single-phase tests at 500 volts (see Table I) also made but current cleared within two cycles.

buses with less than 4-inch spacing, it would be expected that the arc would have more of a tendency to stabilize itself.

2. About half of the line-to-line arcing faults on the 500-volt circuit were unstable and extinguished themselves.

3. The magnitudes of the first-half-cycle values of current are dependent upon the means of initiating the fault. In using a relatively small wire for the initiating means, the current as compared to the bolted-fault current decreases as the circuit current level is increased. On the 500-volt circuits, the results of the copper bar and steel bar tests indicate that a factor of 0.9 might be applied to the bolted symmetrical current for systems whose bolted-fault currents are in excess of 40,000 amperes and a factor of 1.0 for systems whose bolted-fault currents are less than 40,000 amperes. On 250-volt circuits, the factor appears to be smaller.

4. On 500-volt circuits, the sustained currents may be less than 0.8 of the bolted symmetrical currents.

References

1. Simplified Calculations of Fault Currents. **AIEE Committee on Protective Devices.** AIEE Transactions, volume 6, 1942, pages 1133–5.

2. Fault Voltage Drop and Impedance at Short-Circuit Currents in Low-Voltage Circuits, **O. R. Schurig.** AIEE Transactions, volume 60, 1941, pages 479–86.

Discussion

R. S. Ferguson and J. Grotzinger (Goodyear Tire and Rubber Company, Akron, Ohio): After reading this paper the question arises, "Just what sort of fault do we seek protection against when we apply circuit breakers to low-voltage a-c circuits of 500 and 250 volts?" If we are seeking protection against the possibility of a bolted bus fault, whose occurrence may be very rare, the factor 1.25 times the symmetrical short circuit is in order.

The tests which are reported in this paper would seem to indicate that a factor which approaches 1.25, say 1.10, should be used if we are to protect against short circuits which might be caused by faulty connections in control wiring on 500-volt circuits, because the tests indicate a current for the first half cycle which is 1.10 times the symmetrical value. They also indicate that the arc does not clear itself. They do indicate that after the first half cycle the current decreases materially due to the arc impedance, and consequently the circuit breaker will be called upon to clear a considerably smaller value of current.

In the case of 250-volt circuits, a factor considerably less than 1.10 times symmetrical current may be applied, because the tests indicate that both single phase and 3-phase shorts are self-clearing. Whereas the circuit breaker may have to carry 1.10 times symmetrical short-circuit current for one cycle, it will not have to interrupt this current because the arc clears itself. The tests are interesting in that they indicate that a short circuit which is caused by accidentally placing a copper or steel bar across the 500-volt bus will be less than 0.9 times symmetrical short circuit current.

No data is presented to show what happens when a bar is placed across a 250-volt bus. The assumption may safely be made that here too the arc will be self-clearing because of the fact that the bar is thrown violently from its position.

Another interesting fact brought out by the tests is that an insulator flashover which is caused by a deposit of conducting material results in a short circuit of 0.7 times the symmetrical short-circuit current value.

One method of initiating the fault which is not covered by this extensive series of tests is one which we have often wondered about. In order to make these tests complete we think it would be valuable to obtain data on the magnitude of the short-circuit current which is obtained when we get a short circuit in a piece of equipment like a large motor or transformer.

A general conclusion that can be drawn from the facts which are covered by this paper is that a circuit breaker which has an interrupting capacity considerably less than the recommended practice may after all give adequate protection for practically all faults which it will ever be called upon to clear.

R. H. Kaufmann (General Electric Company, Schenectady, N. Y.): The test information reported in this paper constitutes valuable additional knowledge regarding actual fault current magnitude in the presence of arcing bus faults.

The practical significance of these results are judged to be:

1. Measured fault current magnitudes under arcing fault conditions at 250 and 500 volts alternating current in 4-inch spaced bus work closely approach the values predicted by analysis for solid bolted fault conditions.

Analysis (bolted fault), I = 1.25 times symmetrical component of bolted fault current

Observed in 250 volt tests, I = 1.17 times symmetrical component of bolted fault current

Observed in 500 volt tests, I = 1.08 times symmetrical component of bolted fault current

(Note if the arcing fault had occurred at or near cable terminals entering the switchgear structure, the spacing would be less than 4 inches and measured currents likely to be even closer to the bolted vault case)

2. Extends and confirms test data obtained by other investigators.

3. Strengthens the contention that present simplified analysis procedure yields the correct circuit breaker interrupting rating and no large factors of safety are implied even in the presence of arcing faults.

A word of caution relative to interpretation of the summary may be in order.

The test data reported pertains to arcing faults in 4-inch spaced bus work while the title of the paper is more general "Arcing Faults in Low-Voltage A-C Circuits."

Points 1 and 2 about the self-extinguishing character of the arc pertain only to the specific tests in 4-inch spaced bus work. Experience in 208/120 volt networks certainly proves that faults cannot be depended upon to be self-clearing. To lead a reader to the conclusion that faults on a-c circuits of 250 volts or less are self-clearing would be doing a great wrong.

In point 4, the statement that sustained currents may be less than 80 per cent of the bolted fault values as worded might lead to the interpretation that they will be consistently below the 80 per cent value which would be untrue. Circuit breakers obviously must be capable of safely handling the maximum fault current. To be able to safely handle the average fault condition is not enough.

D. L. Beeman (General Electric Company, Schenectady, N. Y.): This paper represents an extension of the work on the effect of arcs on short-circuit current magnitudes in a-c systems, 600 volts and less. It is always desirable to have valuable engineering information of this nature presented for those who wish to make use of it. The paper confirms two facts previously established:

1. That arcs in the short-circuit current path are very erratic in their effect on the total magnitude of short-circuit current.

2. That the arc is substantially like a resistance and thereby tends to reduce the X/R ratio of circuit and hence, the presence of an arc may reduce the magnitude of the d-c component.

Part 3 of the summary of this paper mentions that certain factors might be applied to the bolted-fault currents. This implies using these factors to determine short-circuit currents.

While anyone may use the information in this and other papers for calculating short-circuit currents it is felt that the adoption of this suggestion for general usage would not be advisable for the following reasons:

1. Years have been spent in getting engineers and others to recognize the short-circuit current problem in low-voltage a-c systems. It is still not recognized by the National Electrical Code (NEC) to the point where short-circuit currents are considered in the application of the Code nor is it considered properly in the rating of many protective devices, most notable of which are the NEC fuses. One of the greatest stumbling blocks in obtaining wider recognition of this problem in low-voltage circuit has been the complexity in short-circuit calculating procedures. The work of a former AIEE committee in simplifying these procedures was most helpful in the advancement of this phase of the art. This committee laid down the first simplified rules and they have been followed with pre-eminent success to date.

The addition of factors to account for an erratic phenomena like arc impedance would be a complexity which would tend to defeat the purpose of simplification and thereby tend to retard wider spread recognition on a sound engineering basis of the short-circuit problem in low-voltage circuits.

2. The present generalized procedure is based on assuming a bolted fault of zero impedance at the point of short-circuit. This was chosen because it has been proved by other tests and reiterated by this paper that the maximum available short-circuit current for any given circuit arrangement can be accurately determined if a bolted fault is assumed. When and only when the maximum short-circuit current is known can the user apply circuit protective device or other circuit elements and know that the maximum short-circuit current attainable is within the short-circuit rating of the equipment.

Field experience has also shown that bolted faults do occur, hence, the introduction of a reduction factor to account for arc drop in the general case seems unsound because:

1. It does not show the maximum short-circuit current as pointed out previously.

2. Arcs are so erratic in their behavior that it would be difficult to ever reach agreement as to just how much arc drop was to be taken into account in the general case. There is no disagreement on the procedure when considering bolted faults.

3. Any generalized calculating procedure which does not show maximum short-circuit current will lead to wide spread application of circuit breakers and other circuit equipment whose short-circuit current rating is less than the maximum fault current which can flow in that circuit. Hence, this will subject many installations to the hazards of inadequate circuit breakers, and so forth.

3. The information presented in the paper while valuable and useful to those who wish to study arc phenomena does not in any way detract from the desirability of maintaining the general case on the basis of the bolted fault which gives the maximum current. The general case which is on the basis of the bolted fault provides a means for the general engineering public to select circuit breakers that are fully adequate for any short circuit in a given system without using anything except the simplest rules. Yet, if anyone so desires he is free to use the data in this article or any other to assume any arbitrary reduction factors he sees fit to account for some phenomena that may not always be present in the circuit like arc resistance. When he does apply such arbitrary reduction factors and applies equipment whose short-circuit rating is less than the maximum short-circuit current attainable by a bolted fault, he is completely responsible for his acts should cases arise and equipment failures or injury to personnel result because of short-circuit currents being in excess of apparatus rating. It is interesting to note that the great majority of engineers prefer to apply circuit breakers in such a way that the maximum total short-circuit current available at the point of application of the circuit breaker as calculated by the general rules assuming a bolted fault is within the interrupting ratings of the circuit breaker.

W. J. McLachlan (General Electric Company, Schenectady, N. Y.): The data presented in this paper constitute a substantial addition to that presented by Schurig referred to by the authors. Variances in the results obtained in comparison to the Schurig data no doubt are related to differences in the conditions of the two sets of tests. However, variations in current values obtained in service experience will be much greater than the range of results reported in both of these papers due to the vastly wider range of conditions influencing the fault impedances in actual installations than is feasible to include in such testing programs. Such variable conditions include, among others, the phase-to-phase and phase-to-ground conductor clearances, the spacing to adjacent circuits, the type and confining influence of any enclosures and, particularly, the manner in which the fault is initiated.

With regard to the manner of fault initiation, it appears entirely probable that bolted-fault current values may be much more closely approached and even fully obtained where foreign objects inadvertently are placed in a bus structure in such manner as to not be free to "blow clear" of the conductors as was the case in both the Schurig and the subject paper tests. Such a result may obtain either through the object being wedged in the structure or through its being so placed that magnetic forces are in a direction to hold it against the conductors rather than in a repelling direction.

With such wide divergence between the possible conditions obtaining in actual service faults and the necessarily limited scope of these testing programs, together with the fact that actual equivalent bolted faults have occurred and will no doubt occur again, it is obvious that the criteria for completely safe applications must be the calculated "bolted-fault" currents. It should be clearly understood and recognized that application on the basis of lesser values involves definite risk.

C. F. Wagner and **L. L. Fountain:** Before replying to the discussions in detail we wish to emphasize a statement made in the paper that the primary purpose of these tests was to provide additional data for the use of the AIEE committee concerned with "Calculating Short-Circuit Currents in Low-Voltage Systems." This committee is being reorganized under the sponsorship of the switchgear committee with representation from the industrial power systems and the transmission and distribution committees. The question of recommending factors for use of the industry in applying circuit breakers in low-voltage circuits is entirely their prerogative and rightly so, as action of this character should be based upon the combined experience and information of the manufacturers, users and anyone else who may possess data pertinent to the problem. All are agreed that the factor 1.25 applied to the symmetrical component of the short-circuit current under bolted fault conditions gives a representative value of the rms current averaged over the three phases for this condition. However, these questions arise

"How can a fault of this character occur in service?";

"If the equivalent of bolted faults occur, what is the frequency of their occurrency?";

"Are all faults of the arcing type?"; or

"What is the nature of the current limiting effect of the arc?"

Thus the question of whether the factor 1.25 should be retained for all applications or whether it should be changed involves a matter of judgment in which factors of the foregoing character must be weighed. The present paper is concerned with the phenomena that occur in the presence of an arc. It attempts to shed light on this question and shows definitely that when an arc is involved the currents are significantly smaller than when the fault is bolted. It is hoped

that the committee will find the data presented in this paper of value in their deliberations.

We wish to thank Ferguson and Grotzinger for their discussion as it reflects the point of view of an industrial user. The discussions by Kaufmann, Beeman and McLachlan are largely concerned with upholding the sanctity of the "bolted fault" as a basis of application. We wish to object to the implication of Kaufmann's statement that the "measured fault current magnitudes under arcing fault conditions at 250 and 500 volts alternating current in 4-inch spaced buswork closely approach the values predicted by analysis for solid bolted fault conditions." He then refers to the factor 1.17 (should have been 1.11) for the 250-volt tests and 1.08 for the 500-volt tests. The conditions for which these factors apply are hardly representative arcing faults. By the method in which the tests were made one could get no other result than those indicated. As we continued to add more and more copper wire for the initiating means bolted-fault conditions were obviously approached. These factors in themselves have no significance. By adding still more copper we could easily have obtained factors of 1.15, 1.20 or even 1.25. The actual figure obtained is determined largely by the time required to fuse the wire. The fault initially is a bolted one and just here is where the matter of judgment enters again. Does the fusion of a metallic conductor bolted across the bus or a loose member resting across it represent more closely the condition met in practice?

When Kaufmann refers to the confirmation of tests by previous investigators he probably refers to the work of Schurig. We wish to call attention again to Schurig's data which indicate maximum factors of 0.88 and 1.0 instead of 1.25.

We certainly did not mean in point 4 of the summary that sustained currents would be consistently below 80 per cent of the bolted symmetrical currents and cannot understand how such interpretation could be placed upon the expression "may be less than 0.80."

Steady-State Equivalent Circuits of Synchronous and Induction Machines

GABRIEL KRON
MEMBER AIEE

Synopsis: Equivalent circuits are developed for the various types of salient-pole synchronous and asymmetrical induction machines, expressed along the physical direct and quadrature axes (cross-field theory). The torques again are found by a direct measurement of power. The networks of all types of machines are found as special cases of that of the "primitive" machine. All equivalent circuits along the physical axes are derivable from those along the hypothetical symmetrical component axes (revolving-field theory) by a real transformation. Although the latter types of networks have already been published previously, they are reproduced here for comparison with the new networks and for a unified tensorial treatment of rotating machines and their equivalent circuits.

The Two Basic Reference Frames

IN the theory of single, isolated rotating machines possessing no commutators two basic reference frames usually are assumed.

1. The axes are rigidly connected to the salient-pole structure.

2. Hypothetical "symmetrical component" axes are associated with the foregoing physical axes.

When rotating machines are interconnected with each other or with transmission lines and other stationary structures, additional physical reference frames must be resorted to. Such additional frames are for instance

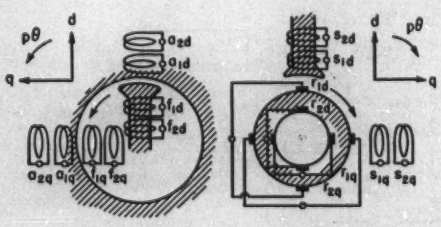
3. The axes rotate with a speed different from that of the rotor.

4. The axes execute small oscillations superimposed upon a steady rotation.

5. There are angular displacements (constant or variable) between the various axes of the same frame.

With each of these physical axes hypothetical symmetrical component axes may be associated.

This paper restricts itself to the systematic development of the steady-state equivalent circuits of single machines when their equations are expressed along the first two types of axes. While the existence of equivalent circuits along the hypothetical symmetrical component axes are widely recognized, it is not so well appreciated that equivalent circuits of equal simplicity may be constructed for any machine along the more physical direct and quadrature axes.



a) THE PRIMITIVE SYNCHRONOUS MACHINE

b) THE PRIMITIVE INDUCTION MACHINE

Figure 1. The primitive rotating machine

The systematic treatment of the equivalent circuits of commutator machines will be based upon the present study and will form the subject matter of a future publication.

The Primitive Machine at Rest

It will be assumed that the equations and equivalent circuits of all machines may be derived as special cases of those of a "primitive" machine. In this machine:

1. One member has a salient pole and asymmetrical windings, the other member is smooth and has symmetrical windings.

2. Depending whether the salient pole is moving or stationary, two such "primitive" machines will be distinguished (Figure 1), namely, the "primitive synchronous" machine and the "primitive induction" machine. The equations and equivalent circuits of both types of primitive machines are identical, differing only by the sign of the speed term v and of the torque T.

3. There exist two independent sets of axes at right angles in space, one along the salient pole (direct axis d) and one at right angles to it (quadrature axis q).

4. Along each axis four windings are assumed, in particular two on the stator and two on the rotor.

When the machine is at rest, each axis may be represented by an arrangement of inductive coils representing the magnetic structure (Figure 2e). See also reference 4 for greater detail. The electrical structure is represented by the resistances and impressed voltages in series.

Equations of the Primitive Machine

The transient equations of the primitive induction machine are (reference 1, page 129) $e = Z \cdot i^1$ where Z is given in Figure 3.

For a synchronous machine $p\theta$ has opposite signs, the subscript s becomes f

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GABRIEL KRON is consulting engineer, General Electric Company, Schenectady, N. Y.

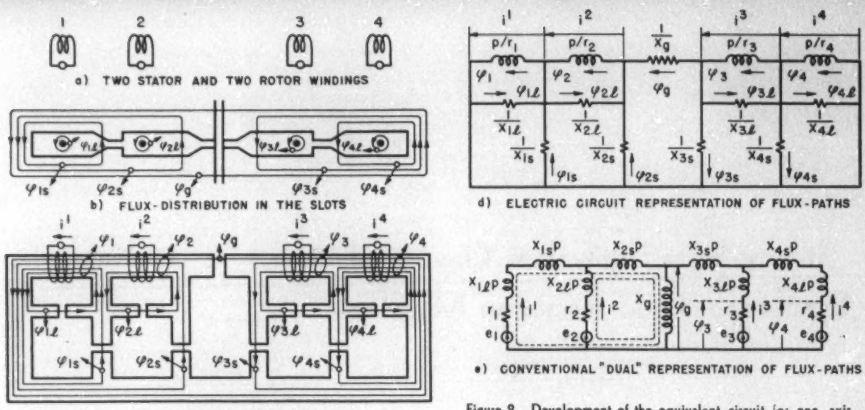


Figure 2. Development of the equivalent circuit for one axis

	d_{s2}	q_{s2}	d_{s1}	q_{s1}	d_{r1}	q_{r1}	d_{r2}	q_{r2}
d_{s2}	$r_{s2d2}+K_{d22}p$		$M_{d2}p$		$M_{d12}p$		$M_{d22}p$	
q_{s2}		$r_{s2q2}+L_{q22}p$		$M_{q2}p$		$M_{q12}p$		$M_{q22}p$
d_{s1}	$M_{d2}p$		$r_{s1d1}+L_{d11}p$		$M_{d11}p$		$M_{d21}p$	
q_{s1}		$M_{q2}p$		$r_{s1q1}+L_{q11}p$		$M_{q11}p$		$M_{q21}p$
d_{r1}	$M_{d12}p$	$M_{q12}p\theta$	$M_{d11}p$	$M_{q11}p\theta$	$r_{r1}+L_{dr11}p$	$L_{qr11}p\theta$	$M_{dr}p$	$M_{qr}p\theta$
q_{r1}	$-M_{d12}p\theta$	$M_{q12}p$	$-M_{d11}p\theta$	$M_{q11}p$	$-L_{dr11}p\theta$	$r_{1r}+L_{qr1}p$	$-M_{dr}p\theta$	$M_{qr}p$
d_{r2}	$M_{d22}p$	$M_{q22}p\theta$	$M_{d21}p$	$M_{q21}p\theta$	$M_{dr}p$	$M_{qr}p\theta$	$r_{r2}+L_{dr22}p$	$L_{qr22}p\theta$
q_{r2}	$-M_{d22}p\theta$	$M_{q22}p$	$-M_{d21}p\theta$	$M_{q21}p$	$-M_{dr}p\theta$	$M_{qr}p$	$-L_{dr22}p\theta$	$r_{r2}+L_{qr2}p$

Figure 3. Impedance matrix of the primitive machine (induction)

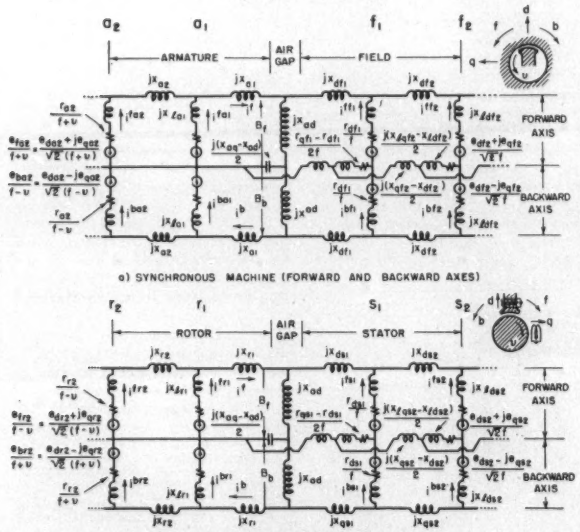


Figure 4. The primitive machine along symmetrical components

(field) and r becomes a (armature). To save space in further manipulations, this $\boldsymbol{Z}$ will be represented as a compound matrix

$$\boldsymbol{Z}=\begin{array}{c|cccc} & s_2 & s_1 & r_1 & r_2 \\ \hline s_2 & \boldsymbol{Z}_{s2} & \boldsymbol{X}_s & \boldsymbol{X}_{r12} & \boldsymbol{X}_{r22} \\ s_1 & \boldsymbol{X}_s & \boldsymbol{Z}_{s1} & \boldsymbol{X}_{r11} & \boldsymbol{X}_{r21} \\ r_1 & \boldsymbol{X}_{r12} & \boldsymbol{X}_{r11} & \boldsymbol{Z}_{r1} & \boldsymbol{X}_r \\ r_2 & \boldsymbol{X}_{r22} & \boldsymbol{X}_{r21} & \boldsymbol{X}_r & \boldsymbol{Z}_{r2} \end{array} \tag{1}$$

where each $\boldsymbol{Z}_r$ matrix has the form

$$\boldsymbol{Z}_r=\begin{array}{c|cc} & d_r & q_r \\ \hline d_r & r_r+L_{dr}p & L_{qr}p\theta \\ q_r & -L_{dr}p\theta & r_r+L_{qr}p \end{array} \tag{2}$$

The other matrices are special cases of $\boldsymbol{Z}_r$ and differ from it in the following manner:

1. The $\boldsymbol{X}$ matrices contain no r terms.
2. The matrices with a subscript s (stator) contain no $p\theta$ terms. The r's in them assume the same form as the L's.

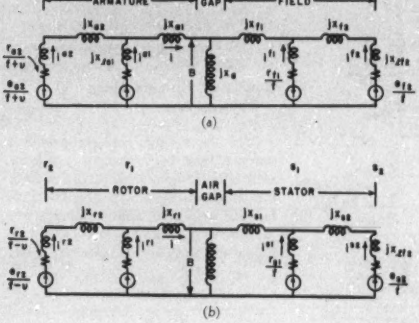


Figure 5. The polyphase primitive machine

(a). Polyphase synchronous machine
(b). Polyphase induction machine

Torque = real of iB^*

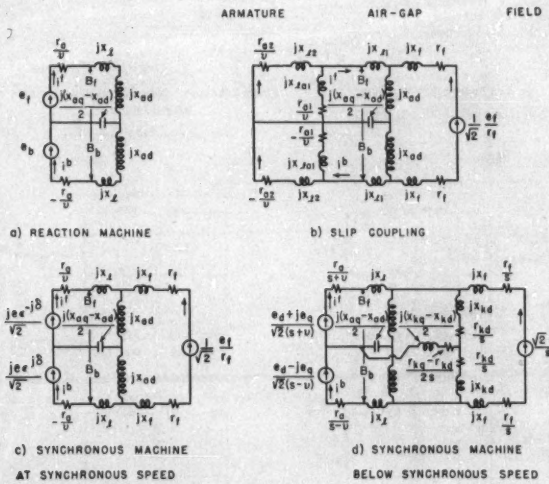


Figure 6. Synchronous machines (forward and backward axes)

Torque = real of $i^f B_f^* + i^b B_b^*$

Similarly the impressed voltage vector is

$$\mathbf{e} = \begin{array}{|c|c|c|c|c|c|c|c|} \hline d_{s2} & q_{s2} & d_{s1} & q_{s1} & d_{r1} & q_{r1} & d_{r2} & q_{r2} \\ \hline e_{ds2} & e_{qs2} & e_{ds1} & e_{qs1} & e_{dr1} & e_{qr1} & e_{dr2} & e_{qr2} \\ \hline \end{array} \tag{3}$$

Represented as a compound matrix, it becomes

$$\mathbf{e} = \begin{array}{|c|c|c|c|} \hline s_2 & s_1 & r_1 & r_2 \\ \hline \mathbf{e}_{s2} & \mathbf{e}_{s1} & \mathbf{e}_{r1} & \mathbf{e}_{r2} \\ \hline \end{array} \tag{4}$$

where

$$\mathbf{e}_r = \begin{array}{|c|c|} \hline d_r & q_r \\ \hline e_{dr} & e_{qr} \\ \hline \end{array} \tag{5}$$

From now on whenever new reference frames will be introduced only this $\mathbf{Z}_r$ (and $\mathbf{e}_r$) matrices will be calculated. The $\mathbf{Z}_s$, $\mathbf{X}_r$, and $\mathbf{X}_s$ matrices will be special cases of $\mathbf{Z}_r$ and will differ from it as just indicated.

During steady state, when the frequency of currents in all axes is not fundamental frequency (unity) but some other frequency f, then in the transient $\mathbf{Z}$

1. p becomes $jf\omega$
2. $p\theta$ becomes $v\omega$
3. ωL and ωM become X

Accordingly $\mathbf{Z}_r$ of equation 2 becomes

$$\mathbf{Z}_r = \begin{array}{c|c|c|} & d_r & q_r \\ \hline d_r & r_r + jfX_{dr} & vX_{qr} \\ \hline q_r & -vX_{dr} & r_r + jfX_{qr} \\ \hline \end{array} \tag{6}$$

Equivalent Circuits Along Symmetrical Components

To bring the matrix $\mathbf{Z}$ of Figure 3 to symmetrical form two transformations are introduced in succession.

1. Symmetrical components are introduced by

$$\mathbf{C}_1 = \begin{array}{c|c|c|c|c|} & s_2 & s_1 & r_1 & r_2 \\ \hline s_2 & \mathbf{C}_s & & & \\ \hline s_1 & & \mathbf{C}_s & & \\ \hline r_1 & & & \mathbf{C}_s & \\ \hline r_2 & & & & \mathbf{C}_s \\ \hline \end{array} \tag{7}$$

where

$$\mathbf{C}_s = \frac{1}{\sqrt{2}} \begin{array}{c|c|c|} & f & b \\ \hline d & 1 & 1 \\ \hline q & -j & j \\ \hline \end{array} \tag{8}$$

As a result each $\mathbf{Z}$, $\mathbf{X}$, and $\mathbf{e}$ is transformed:

$$\mathbf{Z}_r' = \mathbf{C}_{s1}^* \mathbf{Z}_r \mathbf{C}_s \tag{9}$$
$$\mathbf{e}' = \mathbf{C}_{s1}^* \mathbf{e}$$

2. The frequency is reduced to unity by

$$\mathbf{n} = \begin{array}{c|c|c|c|c|} & s_2 & s_1 & r_1 & r_2 \\ \hline s_2 & \mathbf{n}_s & & & \\ \hline s_1 & & \mathbf{n}_s & & \\ \hline r_1 & & & \mathbf{n}_{r1} & \\ \hline r_2 & & & & \mathbf{n}_{r2} \\ \hline \end{array} \tag{10}$$

$$\mathbf{n}_r = \begin{array}{c|c|c|} & f_r' & b_r' \\ \hline f_r & f-v & \\ \hline b_r & & f+v \\ \hline \end{array} \tag{11}$$

As a result each $\mathbf{Z}$, $\mathbf{X}$, and $\mathbf{e}$ is multiplied a

$$\mathbf{Z}_r'' = \mathbf{n}_r^{-1} \mathbf{Z}_r' \tag{12}$$
$$\mathbf{e}'' = \mathbf{n}_r^{-1} \mathbf{e}' \tag{13}$$

Remembering that $\mathbf{n}_s$ contains no v, the final matrices are

$$\mathbf{Z}''_r = \begin{array}{c|c|c|} & f_r & b_r \\ \hline f_r & \dfrac{r_r}{f-v} + j\left(\dfrac{X_{dr}+X_{qr}}{2}\right) & j\left(\dfrac{X_{dr}-X_{qr}}{2}\right) \\ \hline b_r & j\left(\dfrac{X_{dr}-X_{qr}}{2}\right) & \dfrac{r_r}{f+v} + j\left(\dfrac{X_{dr}+X_{qr}}{2}\right) \\ \hline \end{array} \tag{14}$$

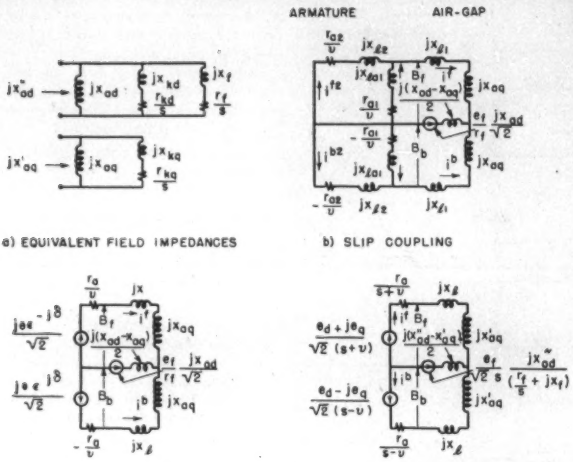


Figure 7. Synchronous machines (forward and backward axes). Field circuits are eliminated

Torque = real of $i^f B_f^* + i^b B_b^*$

$$e_r'' = \boxed{\frac{e_{dr}+je_{qr}}{\sqrt{2}(f-v)} \;\Big|\; \frac{e_{dr}-je_{qr}}{\sqrt{2}(f+v)}} \tag{15}$$

The corresponding equivalent circuits on the two primitive machines are shown on Figure 4. The torques are found by the product of the resultant rotor (or armature) currents i and fluxes E. When the machine structure is smooth, the windings are symmetrical and only polyphase currents and voltages exist, then $i^q = -ji^d$ and only the forward meshes exist. The corresponding polyphase primitive equivalent circuits are shown in Figure 5.

Actual Machines

The equivalent circuits of the various types of synchronous and induction machines shown on Figures 6–8 are special cases of the primitive machine and are found from the latter by simply opening certain meshes. These networks already have been published in references 1–4.

On synchronous machines it is customary to eliminate all field axes and consider only the armature axes. The corresponding simplified circuits are shown in Figure 7. Note that the phase displacement between the excitation and the infinite bus is still the angle δ. The capacitor motor differs from the primitive machine in two respects:

1. Its direction of rotation is from q to d, hence the f axis rotates also from q to d.
2. The ratio of turns on the q_s axis is a.

Equivalent Circuits Along Physical Axes

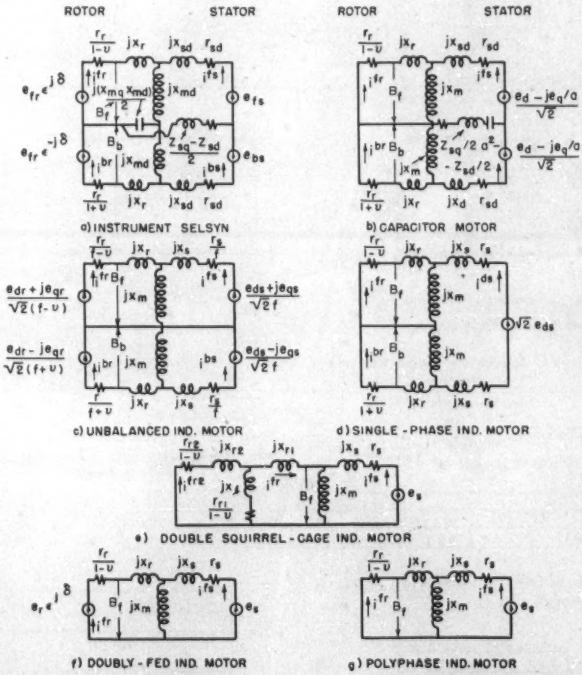
Once the equations of the primitive machine have been brought to a symmetrical form along some particular axes, it is comparatively easy to keep the symmetrical form along other arbitrary axes. A return to the original d and q axes may be accomplished in two successive steps

1. The inverse transformation C_s^{-1} returns to the original physical axes

$$C_s^{-1} = \frac{1}{\sqrt{2}} \begin{array}{c|cc} & d' & q' \\ \hline f & 1 & j \\ b & 1 & -j \end{array} \tag{16}$$

Figure 8 (below). Induction machines (revolving field theory)

Torque = real of $i^{fr} B_f^* + i^{br} B_b^*$



but the resultant $\boldsymbol{Z}$ is not quite symmetrical.

2. The latter may be brought to a symmetrical form by allowing js^q to flow in the network in place of i^q. That is, the additional transformation (changing the time scale in the network along the q axis) is

$$C_2 = \begin{array}{c|cc} & d & q \\ \hline d' & 1 & \\ q' & & -j \end{array} \tag{17}$$

The two transformations may be combined into one as

$$C_3 = C_1^{-1}C_2 = \frac{1}{\sqrt{2}} \begin{array}{c|cc} & d & q \\ \hline f & 1 & 1 \\ b & 1 & -1 \end{array} \tag{18}$$

Transforming $\boldsymbol{Z}_r''$ and $\boldsymbol{e}_r''$ of equations 14 and 15 as

$$\boldsymbol{Z}_r''' = C_{n1}^* \boldsymbol{Z}_r'' C_n \tag{19}$$

$$\boldsymbol{e}''' = C_{n1}^* \boldsymbol{e}'' \tag{20}$$

the resultant matrices are

$$\boldsymbol{Z}_r''' = \begin{array}{c|cc} & d & q \\ \hline d & \dfrac{fr_r}{f^2-v^2}+jd_{dr} & \dfrac{vr_r}{f^2-v^2} \\ q & \dfrac{vr_r}{f^2-v^2} & \dfrac{fr_r}{f^2-v^2}+jX_{qr} \end{array} \tag{21}$$

$$\boldsymbol{e}''' = \begin{array}{c|cc} & d & q \\ \hline & \dfrac{fe_{dr}+jve_{qr}}{f^2-v^2} & \dfrac{ve_{dr}+jfe_{qr}}{f^2-v^2} \end{array} \tag{22}$$

In setting up the resultant $\boldsymbol{Z}'''$ analogously to Figure 3 it should be remembered that

1. The $\boldsymbol{X}'''$ matrices contain no r terms.

2. The matrices with a subscript s contain no v terms.

The corresponding equivalent circuits of the two types of primitive machines are shown in Figure 9. For a polyphase machine (where $i^q = -ji^d$, hence $js^q = i^d$) the equivalent circuit reduces to the same form as that given for symmetrical components, Figure 5, since $i^f = \sqrt{2}\, i^d$. Hence on Figure 5 each i may be considered as representing either i^f or i^d. The same statement applies to e.

For the various types of synchronous and induction machines the equivalent circuits are given in Figures 10 through 12. It should be noted that

1. In the symmetrical component circuits (Figures 4–8) mutual couplings exist between the f and b axes for the *asymmetrical* member only.

2. In the physical circuits (Figures 9–12) mutual couplings exist between the d and q axes for the *symmetrical* member only.

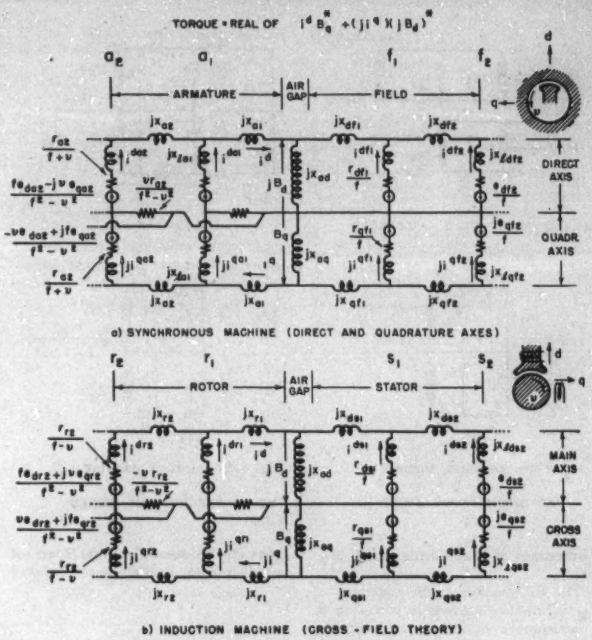
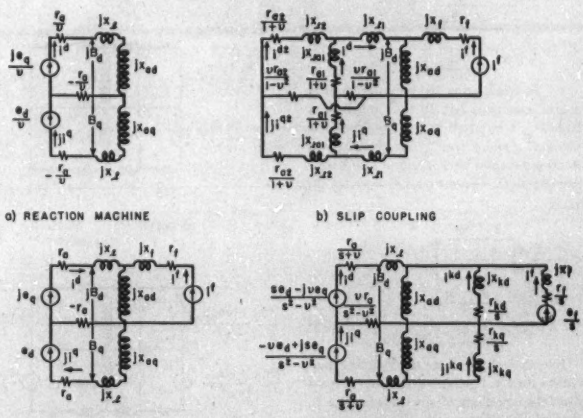


Figure 9. The primitive machine along physical axes

Figure 10. Synchronous machines (direct and quadrature axes)

Torque = real of $i^d B_q^* + (ji^q \times jB_d)^*$



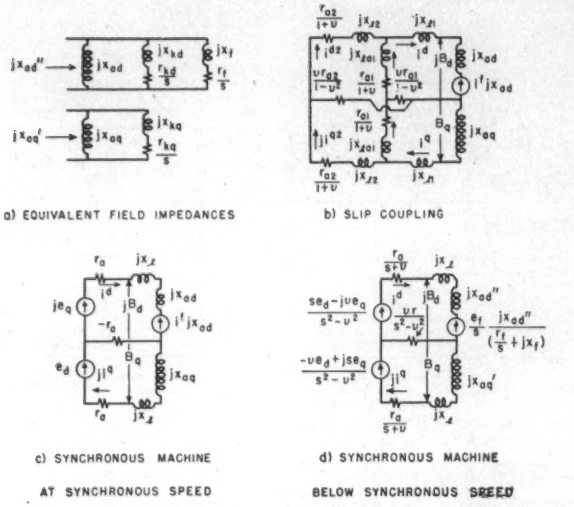
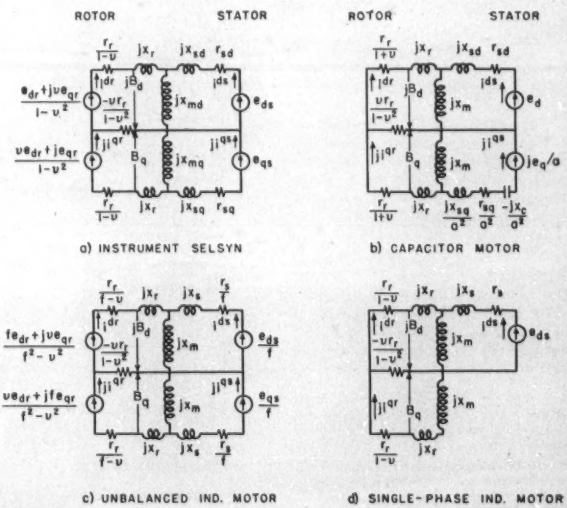


Figure 11. Synchronous machines (direct and quadrature axes). Field circuits are eliminated

Torque = real of $i^d B_q^* + (ji^q)(jB_d)^*$

Figure 12 (below). Induction machines (cross-field theory)

Torque = real of $i^{dr} B_q^* + (ji^{qr})(jB_d)^*$



Summary of Transformations

1. In considering the equations of actual rotating machines it is possible to pass from the physical d and q axes to the hypothetical f and b axes and back again by the transformation C_r and its inverse

$$C_r = \frac{1}{\sqrt{2}} \begin{array}{c|cc} & f & b \\ \hline d & 1 & 1 \\ q & -j & j \end{array}$$

$$C_r^{-1} = \frac{1}{\sqrt{2}} \begin{array}{c|cc} & d & q \\ \hline f & 1 & j \\ b & 1 & -j \end{array} \qquad (23)$$

2. In considering the equations not of actual machines but of their equivalent circuits, it is possible to pass from the physical d and q axes to the hypothetical f and b axes and back again by the transformation C_n (Equation 18) and its inverse

$$C_n = \frac{1}{\sqrt{2}} \begin{array}{c|cc} & f & b \\ \hline d & 1 & 1 \\ q & 1 & -1 \end{array}$$

$$C_n^{-1} = \frac{1}{\sqrt{2}} \begin{array}{c|cc} & d & q \\ \hline f & 1 & 1 \\ b & 1 & -1 \end{array} \qquad (24)$$

Hence, just as all actual rotating machines form a "group" (allowing to pass from the equations of any one machine to that of any other machine by means of a nonsingular C) similarly their equivalent circuits also form a group and it is possible to pass from the equation of any one equivalent circuit to that of any other by means of a C.

Transformations Between Machines and Their Equivalent Circuits

Having shown that all rotating machines form a "group" and that all their equivalent circuits form another group, the question arises in what relations are the two groups to each other.

It may be said in general that while the same currents flow in a machine and in its equivalent circuit, nevertheless different voltages are impressed on them. That is, in going from the equations of a machine to its equivalent circuit the voltages are transformed by $n^{-1} = n_m^{-1}$, but the currents are not. If the subscript m represents a machine and n a network then

$$\left.\begin{aligned} e_n &= n^{-1} e_m \\ e_{\alpha'} &= n_{\alpha'} e_\alpha \end{aligned}\right\} \qquad (25)$$

$$\left.\begin{aligned} Z_n &= n^{-1} Z_m \\ Z_{\alpha'\beta} &= n_{\alpha'}^{\alpha} Z_{\alpha\beta} \end{aligned}\right\} \qquad (26)$$

and the equations of a machine and its equivalent circuit are, respectively,

$$\left.\begin{aligned} e_m &= Z_m i \\ e_\alpha &= Z_{\alpha\beta} i^\beta \end{aligned}\right\} \qquad (27)$$

$$\left.\begin{aligned} e_n &= Z_n i = n^{-1} Z_m i \\ e_{\alpha'} &= Z_{\alpha'\beta} i^\beta \end{aligned}\right\} \qquad (28)$$

Hence the tensors of the group of equivalent circuits are "intermediary tensors" having their indices in two different refer-

ence frames and the two groups are simply subgroups of a more extended group.

The transformation matrices $n = n_n^n$ changing from the network to its machine and back again are as follows:

1. When expressed along symmetrical components

$$n_s = \begin{array}{c|cc|} & f_m & b_m \\ \hline f_n & f-v & \\ b_n & & f+v \\ \hline \end{array}$$

$$n_s^{-1} = \begin{array}{c|cc|} & f_n & b_n \\ \hline f_m & 1/(f-v) & \\ b_m & & 1/(f+v) \\ \hline \end{array} \tag{29}$$

2. When expressed along the physical direct and quadrature axes

$$n_p = C_s n_s C_s^{-1} \tag{30}$$

$$n_p = \begin{array}{c|cc|} & d_m & q_m \\ \hline d_n & f & -jv \\ q_n & jv & f \\ \hline \end{array}$$

$$n_p^{-1} = \frac{1}{f^2 - v^2} \begin{array}{c|cc|} & d_n & q_n \\ \hline d_m & f & jv \\ q_m & -jv & f \\ \hline \end{array} \tag{31}$$

It has to be remembered, though, that at first the time axis of $\bar{s}^n$ in the network equations has to be changed by j as shown in equation 17 before n_p may be used.

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No Discussion

An Induction Generator Used With the All-Electric Automatic Pilot for Airplanes

HERBERT VICKERS
FELLOW AIEE

THIS PAPER deals with the theory of a special type of induction generator from which two demands are made:

(*a*). The voltage in the signal phase must be linearly proportional to the speed at which it is driven.

(*b*). The phase angle between the signal voltage and the reference voltage must not change more than a small fraction of a degree over the speed range.

The machine consists of a stator with slotted core, which accommodates two phases—one is the reference phase, which is supplied with alternating current at a frequency of 400 cycles, and the other phase, in quadrature with the reference phase, is the phase from which the signal voltage is derived. The rotor consists of a conducting sleeve or squirrel cage with a fixed iron core. The requirements (*a*) and (*b*) are met, as will be shown in the analysis, by using a high resistance rotor, and also the leakage fluxes of stator and rotor must be kept small.

The method of attack is briefly as follows:

1. The axis of the reference phase is designated as the X axis.
2. The axis of the signal phase is designated as the Y axis.
3. The flux along the signal phase axis is calculated in terms of the flux along the reference axis.

The real problem is that laid down in item 3. It will be found to involve a linear differential equation of the second order of standard type, whose solution furnishes us with the information we need to attain our desired results.

The reference phase of the stator produces flux only along its axis, namely, the X axis, it is clear that the flux linking the signal phase in the Y axis, can be furnished only by rotor currents. We must therefore find the magnetomotive forces along the X axis in stator and rotor, and also the magnetomotive forces along the Y axis in both rotor and stator.

Consider a coil AB on the rotor, the plane of which makes an angle θ with the X axis at any time t.

Let

ϕ_c = resolved part of the flux in the direction of the coil axis

Then

$$\phi_c = \phi_{RX} \sin\theta - \phi_{RY} \cos\theta \tag{1}$$

where

$$\phi_{RX} = \text{flux in the rotor along the } X \text{ axis} \tag{2}$$

$$\phi_{RY} = \text{flux in the rotor along the } Y \text{ axis} \tag{3}$$

The electromotive force induced in the coil AB, due to variation of ϕ_{RX} and ϕ_{RY} with time and also due to variation of θ with time, is obtained by differentiating equation 1 with regard to time.

$$\phi_c' = \omega \cos\theta\phi_{RX} + \omega \sin\theta\phi_{RY} + \sin\theta\phi_{RX}' - \cos\theta\phi_{RY}' \tag{4}$$

where

$\omega = d\theta/dt$.

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HERBERT VICKERS is consultant with Smith Electric Company, Cricklewood, London, England and Clarke, Chapman and Company, Gateshead on Tyne, England.

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The current in the coil is

$$-\phi_c'/r10^8 \tag{5}$$

where r is the resistance of the coil in ohms; the coil is assumed to have one turn.

The magnetomotive force of the coil is

$$0.4\pi i_c \tag{6}$$

which equals $\mathfrak{F}_c$. Therefore

$$\mathfrak{F}_c = (-4\pi/10^8 r)(\omega\phi_{RX} \cos\theta - \phi_{RY}' \cos\theta + \omega\phi_{RY} \sin\theta + \phi_{RX}' \sin\theta) \tag{7}$$

The resolved part of $\mathfrak{F}_c$ along the X axis, which we may call

$$\delta\mathfrak{F}_{RX} = (-4\pi/10^8 r)(\omega\phi_{RX} \sin\theta \cos\theta - \phi_{RY}' \sin\theta \cos\theta + \omega\phi_{RY} \sin^2\theta + \phi_{RX}' \sin^2\theta) \tag{8}$$

To find the total magnetomotive along the X axis, due to N_2 coils or $2N_2$ rotor bars, we must sum up the magnetomotive forces due to these N_2 coils along the X axis. Therefore

$$\mathfrak{F}_{RX} = (-4\pi/10^8 r)(N_2 \int_0^\pi \sin^2\theta\{\omega\phi_{RY} + \phi_{RX}'\} + \cos\theta \sin\theta\{\omega\phi_{RX} - \phi_{RY}'\} d\theta/\pi \tag{9}$$

$$= -2\pi N_2\{\omega\phi_{RY} + \phi_{RX}'\}/10^8 r \tag{10}$$

Determination of the Magnetomotive Force Along the Y Axis of the Rotor

The resolved part of $\mathfrak{F}_c$ along the Y axis of the rotor is

$$\delta\mathfrak{F}_{RY} = (-4\pi/10^8 r)(-\omega\phi_{RX} \cos^2\theta + \cos^2\theta\phi_{RY}' - \omega\phi_{RY} \sin\theta \cos\theta - \phi_{RX}' \sin\theta \cos\theta) \tag{11}$$

Summing up the resolved parts of the magnetomotive forces along the Y axis for the N_2 coils on the rotor, we have

$$\mathfrak{F}_{RY} = (2\pi N_2/10^8 r)(\omega\phi_{RX} - \phi_{RY}') \tag{12}$$

Let

$$k_2 = 2\pi N_2/10^8 r \tag{13}$$

Then

$$\mathfrak{F}_{RY} = k_2\omega\phi_{RX} - k_2\phi_{RY}' \tag{14}$$

$$\mathfrak{F}_{RX} = -k_2\omega\phi_{RY} - k_2\phi_{RX}' \tag{15}$$

Let

R = air-gap reluctance

and

$$\alpha = R/k_2 = 10^9 rR/2\pi N_2 \tag{16}$$

Equations 14 and 15 enable us to obtain the relation between ϕ_{SY} and ϕ_{SX} which we need. The derivation is given in Appendix I.

The result is

$$\phi_{SY} = \omega\phi_{SXM} \sin(\omega_0 t - \lambda) / \sqrt{\left[\{(1+\sigma_r)k_2(\sigma_r+\sigma_r\sigma_s+\sigma_s)/R\} \times \{\omega^3-\omega_0^3\} + R(1+\sigma_s)/k_2\right]^2 + [1+2\sigma_r+2\sigma_s+2\sigma_r\sigma_s]^2\omega_0^2} \tag{17}$$

$$\tan\lambda = (1+2\sigma_r+2\sigma_s+2\sigma_r\sigma_s)\omega_0 / \{(1+\sigma_r)k_2(\sigma_r+\sigma_r\sigma_s+\sigma_s)/R\}\{\omega^3-\omega_0^3\} + R(1+\sigma_s)/k_2 \tag{18}$$

ϕ_{SXM} = maximum value of the flux in the X axis of the stator

λ = angle of lag of ϕ_{SY} behind ϕ_{SX}

ω = angular velocity of the rotor in radians per second

$\omega_0 = 2\pi \times$ supply frequency

$\sigma_r = RP_r$ = leakage flux in rotor ÷ useful flux in rotor

$\sigma_s = RP_s$ = leakage flux in stator ÷ useful flux in stator in X axis

Equation 17 shows that ϕ_{SY}, which is the flux in the signal axis, is nearly proportional to the speed, and the more nearly so as the term involving ω in the denominator is negligibly small.

This will be so provided

$$k_2/R = 1/\alpha = 2\pi N_2/10^9 rR$$

is small. Clearly r must be large and also R must be as large as is permissible. A high resistance rotor is, therefore, necessary, and the air-gap reluctance must be as great as the heating limits permit. Since, also, the phase shift of ϕ_{SY} with respect to ϕ_{SX}, as the speed varies, also must be very small, equation 18 shows that the same condition must be satisfied as for the first requirement, that is, that ϕ_{SY} shall be proportional to the speed.

It is clear from equation 18 that tan λ will be sensibly independent of the speed, provided k_2 is small, that is, provided we have a high resistance rotor.

Results of Investigation

Our investigation shows that we need some material for the rotor having high resistivity and low temperature coefficient. Generators have been built having Eureka bars and brass end rings, but successful results have also been achieved by using a high resistance sleeve of German silver or brass.

Very accurate centering of the rotor is necessary, and the thickness of the cylinder or sleeve must be quite uniform. Also, it is desirable to keep the leakage fluxes in stator and rotor small.

Formulas for $\mathfrak{F}_{SX}$, $\mathfrak{F}_{RX}$, ϕ_{SX}, E_{SY}, and I_S

In order to determine completely the characteristics of such a generator, we need to obtain expressions for the magnetomotive forces $\mathfrak{F}_{SX}$ and $\mathfrak{F}_{RX}$, and also for ϕ_{SX} and the signal voltage E_{SY} and the stator current.

All these quantities are calculated in the appendixes, and we merely will give the results here.

The procedure is to calculate $\mathfrak{F}_{SX}$ and $\mathfrak{F}_{RX}$ in terms of ϕ_{SX} and then to determine ϕ_{SX} from the voltage and frequency applied to the machine and its known winding data.

From $\mathfrak{F}_{SX}$ and $\mathfrak{F}_{RX}$ the stator and rotor currents are readily determined, and also the various phase angles of these quantities. Knowing ϕ_{SY}, one can determine the number of turns per coil required on the signal axis.

$$\mathfrak{F}_{SX} = [-k_2\omega(1+\sigma_r) - RD/\omega - k_2(1+\sigma_r)D^3/\omega]\phi_{SY} \tag{19}$$

ϕ_{SY} is given in equation 17. By substituting the value given in equation 17 for ϕ_{SY} and operating on it by the three terms in the bracket, we obtain $\mathfrak{F}_{RX}$

$$\mathfrak{F}_{SX} = (R/\omega)([(1+2\sigma_r)(\omega^3+D^3)/\alpha + 2D(1+\sigma_r)+\alpha])\phi_{SY} \tag{20}$$

Formula for ϕ_{SX}

$$v = \phi_{SXM} \times k \sin(\omega_0 t + \delta) \tag{21}$$

$$\phi_{SXM} = V_m/k$$

where

N_s = number of turns in series in signal phase

k_1 = breadth factor for the fundamental

k_4 = coil-span factor for the fundamental

$$E_{SX} = \sqrt{2}\pi \times k_1 k_4 \phi_{SYM} \times N_s \times f/10^8 \tag{22}$$

where

k_4 = coil span factor for fundamental

f = supply frequency

The current is determined from the magnetomotive force by equation 61 as follows:

$$\mathfrak{F}_{SXM} = 1.8 N_1 \times k_1 \times k_4 \times I/p \tag{23}$$

It is clear that this method can be applied to the calculation of single-phase induction motors. Its application is simple and direct, and much shorter than would seem from the analysis.

Appendix I

Proof of Equation 17 for ϕ_{SY} in Terms of ϕ_{SX}

From equations 14 and 15

$$\mathfrak{F}_{RY} = k_2\omega\phi_{RX} - k_2\phi_{RY}' \tag{14}$$

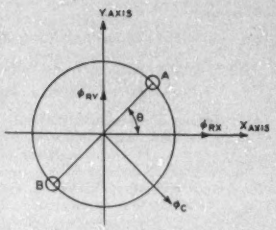

Figure 1. Derivation of the magnetomotive force in the X axis of the rotor

$$\mathfrak{F}_{RX} = -k_2\omega\phi_{RY} - k_2\phi_{RX}' \tag{15}$$

$$\phi_{RX} = \phi_{SX} - \phi_{LX} \tag{24}$$

ϕ_{LX} = stator plus rotor leakage flux in the X axis

$$= \mathfrak{F}_{SX}P_{SX} - \mathfrak{F}_{RX}P_{RX} \tag{25}$$

P_{SX} = equivalent permeance of stator leakage path (26)

P_{RX} = equivalent permeance of rotor leakage path (27)

$$\mathfrak{F}_{RY} = k_2\omega[\phi_{SX} - \mathfrak{F}_{SX}P_{SX} + \mathfrak{F}_{RX}P_{RX}] - k_2\phi_{RY}' \tag{28}$$

$$\phi_{RY} = \phi_{SY} + \mathfrak{F}_{RY}P_{RY} = \phi_{SY} + \mathfrak{F}_{RY}P_{RX} \tag{29}$$

This simply states that the rotor flux in the Y axis is the sum of the stator flux in the Y axis plus the rotor leakage flux in the Y axis. Therefore

$$\phi_{RY} = \mathfrak{F}_{RY}/R + \mathfrak{F}_{RY}P_{RX} = \mathfrak{F}_{RY}[1+RP_{RX}]/R = \mathfrak{F}_{RY}(1+\sigma_r)/R \tag{30}$$

$$\phi_{RY}R/(1+\sigma_r) = k_2\omega[\phi_{SX} - P_{SX}(R\phi_{SX} - \mathfrak{F}_{RX})/(1+\sigma_s) + \mathfrak{F}_{RX}P_{RX}] - k_2\phi_{RY}' \tag{31}$$

for

$$\phi_{SX} = (\mathfrak{F}_{SX} - \mathfrak{F}_{RX})(1+\sigma_s)/R \tag{32}$$

$$\phi_{RY}R/(1+\sigma_r) = k_2\omega\phi_{SX}/(1+\sigma_s) + \{k_2\omega\sigma_r/R + k_2\omega\sigma_s/R(1+\sigma_s)\}\{-k_2\omega\phi_{RY} - k_2\phi_{RX}'\} - k_2\phi_{RY}' \tag{33}$$

This step will be clear, if we remember that

$$P_{SX}R = \sigma_s$$

and

$$P_{RX}R = \sigma_r$$

Therefore

$$\phi_{RY}R/(1+\sigma_r) = A\phi_{SX} - B\phi_{RY} - C\phi_{RX}' - k_2\phi_{RY}' \tag{34}$$

$$A = k_2\omega/(1+\sigma_s) \tag{35}$$

$$B = k_2\omega[k_2\omega\sigma_r/R + k_2\omega\sigma_s/R(1+\sigma_s)] \tag{36}$$

$$C = B/\omega \tag{37}$$

Now substitute the value of ϕ_{RX}' derived from equation 14

$$\phi_{RX} = \mathfrak{F}_{RY}/k_2\omega + \phi_{RY}'/\omega \tag{38}$$

$$= \phi_{RY}R/(1+\sigma_r)k_2\omega + \phi_{RY}'/\omega \tag{39}$$

Therefore

$$\phi_{RX}'' = \phi_{RY}'R/(1+\sigma_r)(k_2\omega) + \phi_{RY}''/\omega \quad (40)$$

$$A\phi_{SX} = C\phi_{RY}''/\omega + \phi_{RY}'\{CR/k_2\omega(1+\sigma_r) + k_2\}+(\phi_{RY}R)/(1+\sigma_r) + B\phi_{RY} \quad (41)$$

$$\phi_{XY} = A\omega\phi_{SX}/CD^2 + \{k_2\omega + CR/k_2(1+\sigma_r)\} D + R\omega/(1+\sigma_R) + B\omega \quad (42)$$

where

$$D = d/dt + D^2 = d^2/dt^2 \quad (43)$$

Now

$$C/A = k_2\{\sigma_r + \sigma_r\sigma_s + \sigma_s\}/R \quad (44)$$

and

$$B/A = k_2\omega\{\sigma_r + \sigma_r\sigma_s + \sigma_s\}/R \quad (45)$$

but

$$\phi_{SY} = \phi_{RY}/(1+\sigma_r) \quad (46)$$

Therefore

$$\phi_{SY} = \omega\phi_{SXM}\sin(\omega_0 t - \lambda)/\times \sqrt{[\{(1+\sigma_r)k_2(\sigma_r+\sigma_r\sigma_s+\sigma_s)/R\}\{\omega^2 - \omega_0^2\} + R(1+\sigma_s)/k_2]^2 + [1+2\sigma_r + 2\sigma_s + 2\sigma_r\sigma_s]^2\omega_0^2} \quad (47)$$

and

$$\tan\lambda = (1+2\sigma_r+2\sigma_s+2\sigma_r\sigma_s)\omega_0/\{(1+\sigma_r)k_2(\sigma_r+\sigma_r\sigma_s+\sigma_s)/R\}\{\omega^2-\omega_0^2\} + R(1+\sigma_s)/k_2 \quad (48)$$

If one neglects the products and squares of ρ_r and ρ_s one obtains

$$\phi_{SY} \cong \omega\phi_{SXM}\sin(\omega_0 t - \lambda)/ \sqrt{[\{(\sigma_r+\sigma_s)(\omega^2-\omega_0^2)/\alpha\} + \alpha(1+\sigma_s)]^2 + [1+2\sigma_r+2\sigma_s]^2\omega_0^2} \quad (49)$$

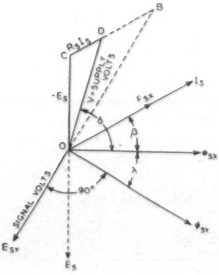

Figure 2. Vector diagram for machine

E_s = counterelectromotive force induced in the stator reference phase by ϕ_{SX}

$DE = R_s I_s$ = volts drop in the external resistance to bring supply volts in line with the signal volts

This diagram shows the phase relations of the various quantities. ϕ_{SY}, F_{SX}, F_{RX}, I_s are all determined with reference to ϕ_{SX}, and ϕ_{SX} is then determined with reference to the applied volts

All these quantities and their phase angles are determined from the various equations for them given in the appendixes

and

$$\tan\lambda \cong (1+2\sigma_r+2\sigma_s)\omega_0/\{(\sigma_r+\sigma_s)(\omega^2-\omega_0^2)/\alpha\} + \alpha(1+\sigma_s) \quad (50)$$

Appendix II

Determination of the Magnetomotive Force in the X Axis of the Rotor $\mathfrak{F}_{RX}$

From equation 15

$$\mathfrak{F}_{RX} = -k_2\omega\phi_{RY} - k_2\phi_{RX}'$$

where

$$\phi_{RX}' = d/dt(\phi_{RX})$$

$$\mathfrak{F}_{RX} = -k_2\omega\phi_{RY} - k_2[\mathfrak{F}_{RY}' + k_2\phi_{RY}'']/k_2\omega \quad (51)$$

$$= -k_2\omega\phi_{RY} - R\phi_{SY}'/\omega - k_2\phi_{RY}''/\omega \quad (52)$$

since

$$\mathfrak{F}ry' = \phi_{SY}'R$$

$$\mathfrak{F}_{RX} = -k_2\omega(1+\sigma_r)\phi_{SY} - R\phi_{SY}'/\omega - k_2(1+\sigma_r)\phi_{SY}''/\omega \quad (53)$$

$$= [-k_2\omega(1+\sigma_r) - RD/\omega - k_2(1+\sigma_r)D^2/\omega]\phi_{SY} \quad (54)$$

We already have obtained ϕ_{SY} in terms of ϕ_{SX} and thus $\mathfrak{F}_{RX}$ is obtained by operating on ϕ_{SY} (expressed in terms of ϕ_{SX} by equation 47) by the factor in square brackets.

Equation 52 also may be expressed thus

$$\mathfrak{F}_{RX} = [-R\omega^2(1+\sigma_r)/\alpha - RD - R(1+\sigma_r)\times D^2/\alpha]\phi_{SXM}\sin(\omega_0 t - \alpha)/ \sqrt{(\ )^2 + (\ )^2} \quad (55)$$

Appendix III

Determination of $\mathfrak{F}_{SX}$, the Magnetomotive Force in the X Axis of the Stator

$$\phi_{SX} = \{(\mathfrak{F}_{SX} + \mathfrak{F}_{RX})/R\} + \mathfrak{F}_{SX}P_{SX} \quad (56)$$

that is, the total flux in the X axis of the stator is equal to the flux in the X axis which crosses the air gap, plus the stator leakage flux.

$\mathfrak{F}_{RX}$ will be negative in any numerical example. It is the component of the rotor magnetomotive force measured in the positive direction of the X axis.

$$\mathfrak{F}_{SX} = (R\phi_{SX} - F_{RX})/(1+\sigma_s) \quad (57)$$

since

$$RP_{SX} = \sigma_s$$

but

$$\phi_{SX} = [\{(1+\sigma_r)(\sigma_r+\sigma_r\sigma_s+\sigma_s)/\alpha\} + D^2] + D(1+2\sigma_r+2\sigma_r\sigma_s+2\sigma_r\sigma_s) + \alpha(1+\sigma_s)]\phi_{SY}/\omega \quad (58)$$

and

$$\mathfrak{F}_{RX} = R[(\omega^2+D^2)(2\sigma_r+1)/\alpha + 2D(1+\sigma_r) + \alpha]\phi_{SY}/\omega \quad (59)$$

Therefore

$$\mathfrak{F}_{SX} = R[(\omega^2+D^2)(2\sigma_r+1)/\alpha + 2D(1+\sigma_r) + \alpha]\phi_{SXM}\sin(\omega_0 t - \lambda)/\sqrt{(\ )^2 + (\ )^2} \quad (60)$$

$$\mathfrak{F}_{RX} = R[-\omega^2(1+\sigma_r)/\alpha - D - (1+\sigma_r)D^2/\alpha]\phi_{SY}/\omega \quad (61)$$

$$\mathfrak{F}_{SX} = R[(\omega^2+D^2)(1+2\sigma_r)/\alpha + 2D(1+\sigma_r) + \alpha]\phi_{SXM}\sin(\omega_0 t - \alpha)/ \sqrt{(\ )^2 + (\ )^2} \quad (62)$$

Thus the magnetomotive force is obtained by operating on ϕ_{SY} (which is expressed in terms of ϕ_{SX}) by the operator to the left.

Appendix IV

Determination of ϕ_{SX} in Terms of the Applied Voltage

Let

$$v = V_m \sin(\omega_0 t + \delta)$$

be the instantaneous value of the applied volts.

ϕ_{SX} is the reference vector, and V_m leads ϕ_{SX} by the angle δ

Then

$$v = N_1\phi_{SX}'k_1k_2/10^8 + R_s i \quad (63)$$

N_1 = number of turns in stator reference phase

k_1 = breadth factor of the stator winding for the fundamental

k_2 = coil span factor

For a single-phase winding, the amplitude of the magnetomotive force wave in ampere-turns is

$$\mathfrak{F}_{SXM} = 1.8N_1k_1k_2I/p \quad (64)$$

p = number of poles

R_s = resistance of reference phase

I = rms value of current in reference phase

Now $\mathfrak{F}_{SX}$ will be obtained in the form

$$a\phi_{SXM}\sin(\omega_0 t + \beta) \quad (65)$$

Therefore

$$v = N_1k_2k_1\omega_0\phi_{SXM}\cos\omega_0 t/10^8 + R_s a\phi_{SXM}\sin(\omega_0 t + \beta)/1.8N_1k_1k_2/p \quad (66)$$

Let

$$c = N_1k_1k_2\omega_0/10^8 \quad (67)$$

and

$$d = R_s a/(1.8N_1k_1k_2/p) \quad (68)$$

then

$$v = c\phi_{SXM}\cos\omega_0 t + d\phi_{SXM}\sin(\omega_0 t + \beta) \quad (69)$$

$$= c\phi_{SXM}\cos\omega_0 t + d\phi_{SXM}\sin\omega_0 t\cos\beta + d\phi_{SXM}\cos\omega_0 t\sin\beta \quad (70)$$

$$= \phi_{SXM}[(c + d\sin\beta)\cos\omega_0 t + d\cos\beta\times\sin\omega_0 t] \quad (71)$$

$$= \phi_{SXM}[f\cos\omega_0 t + g\sin\omega_0 t] \quad (72)$$

$$f = c + d\sin\beta \quad (73)$$

$$g = d\cos\beta \quad (74)$$

then

$$v = \phi_{SXM}k\sin(\omega_0 t + \delta) \quad (75)$$

where

$$k\cos\delta = g \quad (76)$$

and

$$k \sin \delta = f \tag{77}$$

$$k = \sqrt{g^2+f^2} \tag{78}$$

and

$$\tan \delta = f/g \tag{79}$$

Therefore

$$k = \sqrt{d^2 \cos^2 \beta + c^2 + 2cd \sin \beta + d^2 \sin^2 \beta} \tag{80}$$

$$= \sqrt{c^2+d^2+2cd \sin \beta} \tag{81}$$

and

$$\tan \delta = (c+d \sin \beta)/d \cos \beta \tag{82}$$

Now

$$V_m = \phi_{SXM} \times k \tag{83}$$

Therefore

$$\phi_{SXM} = V_m/k = V_m/\sqrt{c^2+d^2+2cd \sin \beta} \tag{84}$$

From equation 82 we can find the amplitude of ϕ_{SX} in terms of the amplitude of applied volts, namely, V_m, and also the phase angle δ from equation 77 between V_m and ϕ_{SX}.

Appendix V. List of Symbols

ϕ_{SX} = flux in the stator in the X axis
ϕ_{RX} = flux in the rotor in the X axis
ϕ_{LX} = leakage flux in stator and rotor in the X axis
ϕ_{RY} = rotor flux in the Y axis
ϕ_{SY} = stator flux in the Y axis
$\mathfrak{F}_{SX}$ = magnetomotive force of stator in the X axis
$\mathfrak{F}_{RX}$ = magnetomotive force of the rotor in the X axis
N_1 = number of turns in series in reference phase
N_2 = number of turns on the rotor
N_3 = number of turns in series in the signal phase
r = resistance of each rotor bar
$k_2 = 2\pi N_2/10^9 r$
$\alpha = 10^9 r R/2\pi N_2$
R = air-gap reluctance
P_r = permeance of rotor leakage path
P_s = permeance of stator leakage path
$\sigma_r = RP_r$ = leakage flux in rotor/useful flux in rotor
$\sigma_s = RP_s$ = leakage flux in stator/useful flux in stator
k_1 = breadth factor of the stator winding for the fundamental
k_s = coil span factor of the stator winding for the fundamental
N_1 = number of turns in series in the stator reference phase winding
R_s = resistance of reference phase of stator winding
I = rms value of stator current
Maximum value of stator current
i = instantaneous value of stator current
p = number of poles
v = instantaneous value of applied voltage
V = rms value of applied volts
V_m = maximum value of applied volts
$\omega_0 = 2\pi \times$ supply frequency
ω = angular velocity of rotor in radians per second
$c = N_1 k_1 k_{s\omega_0}/10^8$
$d = R_s a p/1.8 N_1 k_1 k_s$
a = factor in expression $\mathfrak{F}_{SX} = a\phi_{SXM} \sin (\omega_0 t+\beta)$
$f = c + d \sin \beta$
$g = d \cos \beta$
$k = \sqrt{g^2+f^2}$
$\tan \delta = f/g$
θ = angle between X axis and plane of the coil on the rotor in Figure 1
β = angle between ϕ_{SX} and $\mathfrak{F}_{SX}$ or I
λ = angle between ϕ_{SX} and ϕ_{SY}
δ = angle between ϕ_{SX} and V

No Discussion

Single-Phase Synchronous Machine

I. A. TERRY
FELLOW AIEE

B. L. ROBERTSON
FELLOW AIEE

Introduction

THIS PAPER describes the theory of the performance of single-phase synchronous machines and presents test results in verification of the theory. The material in the paper is based upon work done by the authors quite a number of years ago, and is presented as an extension of the use of the two-reaction theory of analysis of synchronous-machine performance.

Synchronous machines in central stations, in industrial plants, and in general commercial use, are primarily three-phase. There are, however, a number of applications of synchronous machines, both as motors and generators, in which single-phase power is used quite advantageously. The outstanding illustrations lie in the fields of railway electrification and high-frequency heating where single-phase synchronous machines have a great importance. There are a number of railway applications which have been in use for many years, and more locomotives have been added recently in some of these applications. The authors believe that the fundamental nature of the analysis in the paper will produce a better understanding of the performance of single-phase machines, will fill a gap in the present published literature on synchronous machines, and hence make the paper of permanent value.

Types of single-phase power and single-phase synchronous machines in commercial use are considered first. Following this there is given a theoretical analysis of single-phase synchronous machines. In this analysis steady-state conditions are considered first, based on the classical assumption of no saturation and no stator hysteresis or eddy-current losses, treating the machine generally as a three-phase machine with direct and quadrature axes of symmetry and with one phase winding left idle. Considerations are also given to the effects of sudden changes in loading, sudden changes in circuit, starting performance, and load losses.

Types of Single-Phase Power and Synchronous Machines

The most important and perhaps the largest kilowatt usage of single-phase power is in railway electrification. There are a number of installations which have been in service for many years. Other uses of commercial-frequency single-phase power from 25 to 60 cycles are confined principally to small isolated plants, and for testing facilities.

The use of polyphase power for major railway electrification has been limited because of the complicated mechanical arrangements necessary for two high-voltage trolley wires instead of one, and the resultant high cost. Major electrifications have been 11,000-volt single-phase trolleys with step-down transformers to series-type 25-cycle traction motors. Another important installation uses a system that includes a single-phase motor-generator set on the locomotive. This consists of a high-voltage single-phase synchronous motor driving a d-c generator. Electric power is conducted from the generator to the d-c traction motors. One of the important advantages of this motor-generator system, from the viewpoint of power generation and transmission, is the power-factor correction obtainable from an over-excited synchronous motor.

There are three general methods of providing 25-cycle single-phase power to the railway trolleys. One is by means of a frequency-converter set operating from a 60-cycle system. This type of installation has resulted because existing 60-cycle power stations have been called upon to furnish relatively large blocks of single-phase power. Another method is the use of single-phase, 25-cycle, vertical waterwheel-driven generators or by the use of single-phase steam-turbine-driven generators. Important installations of both of these power sources are in operation. A third method of obtaining single-phase power is to take it directly from a polyphase system of the same frequency and accept the resulting unbalance if the system will stand it, or correct the unbalance by means of a phase converter. The phase converter is similar in operation to a condenser except that it furnishes negative-sequence current instead of positive-sequence current to the system. With it, perfect balance of the three-phase system may not be obtainable under all conditions of load and at all points, but a relatively large three-phase system as compared with the single-phase load can frequently be balanced near enough for practical purposes. It is understood that one foreign railway company is embarking currently on light branch-line electrification using a single-phase, 50-cycle trolley voltage which is derived from the commercial, 50-cycle, 3-phase power supply system without frequency changers.

There is in commercial operation a variation of this phase-converter system. Twenty-five-cycle single-phase power is taken from the trolley to a polyphase transformer bank on the locomotive to which is connected a three-phase induction motor operating as a phase balancer. The three-phase traction motors receive their power from this transformer-balancer system.

In addition to the use of single-phase power for railroads there are a few low-frequency applications and a number of high-frequency applications.

The use of low-frequency power is confined almost entirely to induction fur-

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I. A. TERRY is assistant manager of engineering, fractional horsepower motor divisions, General Electric Company, Fort Wayne, Ind., and B. L. ROBERTSON is professor of electrical engineering, University of California, Berkeley, Calif.

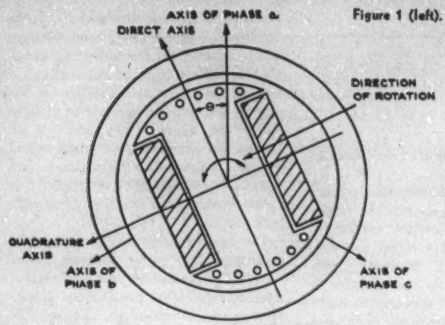


Figure 1 (left). Two-pole diagram of synchronous machine

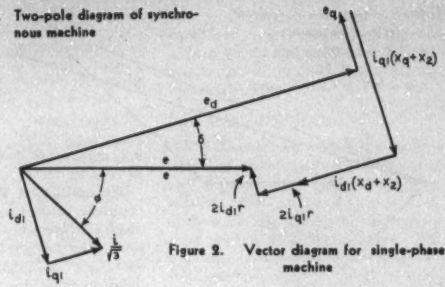


Figure 2. Vector diagram for single-phase machine

naces, but this type of furnace is rapidly becoming obsolete. In such apparatus the frequency is usually 15 cycles per second or less. The furnace is of the core type utilizing the principle of high flux density and a low rate of change of density to obtain the desired heating effects. The power factor of such a system is low, usually four-tenths to five-tenths, so that from the standpoint of kilovolt-ampere–kilowatt efficiency the scheme is rather uneconomical. The cost of static condensers to improve the power factor would be entirely prohibitive at such a low frequency.

There are two general uses of high-frequency single-phase power. One is for testing; for this, sine-wave generators usually supply the power, principally for testing cables, static capacitors, insulators, and some turn testing of electric machinery coils. The other application is to high-frequency induction furnaces. This type of furnace utilizes the principle of low flux density and high rate of change of density. The power factor is inherently lower than in low-frequency applications but, because of the high frequency, static condensers can be used to very good advantage.

A third use of high-frequency single-phase power is for the carrier-current supply for radio and similar purposes.

Design Features of Single-Phase Synchronous Machines

Low-frequency and usual commercial-frequency single-phase machines are of salient-pole construction. High-frequency sine-wave machines usually have cylindrical-rotor construction with a single-layer or double-layer type of field winding. The high-frequency machines for supplying power for induction furnaces sometimes have salient poles, and sometimes are of the inductor type. The very high-frequency machines used for carrier current are of the inductor type. In a polyphase system the power flows at a constant rate. In a single-phase system the power pulsates at twice line frequency. In a synchronous machine the single-phase current can be considered as pulsating at line frequency or it can be considered as comprising two rotating components, one rotating at synchronous speed in the forward direction and the other rotating at synchronous speed in the backward direction. The backward-rotating component induces twice-line-frequency currents into the rotor causing rotor heating which is well known as the limiting design feature of single-phase machines. All single-phase units, therefore, require amortisseur-winding effects to provide a low-resistance path for the induced currents. The higher the specific armature loading of the machine the larger in proportion the amortisseur-winding effect must be. For this reason, salient-pole machines are equipped with pole-face windings. As a result of skin effect the resistance of a bar imbedded in iron increases with the frequency and with the bar diameter. Therefore, the resistance is not inversely proportional to the area and there is a size beyond which it is practically useless to increase the diameter of a bar unless means are adopted for obtaining uniform current distribution from top to bottom. This has been accomplished in some cases by using transposed amortisseur-winding bars.

Cylindrical-rotor machines have pole-face windings consisting of conducting wedges electrically connected at the machine ends. In addition, sections of the field winding can be short-circuited to obtain still lower resistance to the backward-moving field.

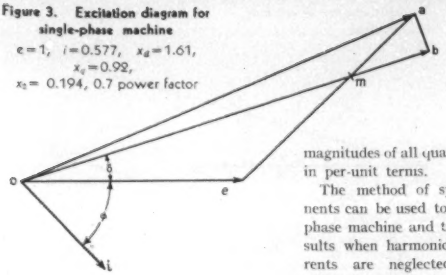
High-frequency salient-pole machines show very good amortisseur characteristics in the main field winding alone, hence no rotor bar winding is provided. Further, skin effect would render such a winding quite inoperative. Inductor-type high-frequency alternators depend upon stray leakage paths for the necessary rotor-current damping. Because the number of poles is large, a very small mechanical angular oscillation will generate the necessary amortisseur-winding effect.

As the power in a single-phase system pulsates, the torque on the machine rotor and stator also pulsates. This impresses variable forces upon the supporting structure. In large units these forces are so great that it is necessary to provide an elastic or spring mounting between the frame and the foundation to eliminate excessive fatigue stresses. Except in very small machines the rotor inertia is so large that no speed variations due to the pulsating torque are discernible.

The starting performance of a single-phase synchronous motor severely limits its application. With single phase there is no net forward torque developed at standstill although there may be forward torque as soon as rotation commences. This increases with speed so that from above 40 per cent normal speed there is frequently enough net torque to continue the acceleration. Split-phase starting has been used with some degree of success, particularly in induction motors, but with the usual type of starting equipment consisting of a resistance and a reactance with no capacitor. The torque efficiency in this case is only about one-third that of the similar three-phase motor. By using a capacitor the torque efficiency can be materially increased, and with recent capacitor developments this has become a standard commercial practice in very small sizes, particularly with induction motors. One method used in motor-generator sets for railway service is to provide a starting motor to bring the set above half speed after which the single-phase power is supplied to the synchronous motor and the set attains synchro-

Figure 3. Excitation diagram for single-phase machine

$e=1$, $i=0.577$, $x_d=1.61$, $x_q=0.92$, $x_2=0.194$, 0.7 power factor



nism. Another method that has been used in some cases is to connect the d-c generator as a series motor for starting.

Single-phase generators are usually simply three-phase Y-connected machines with suitable rotor construction and one of the stator phases left idle. The idle phase may be considered as an insurance or protection in case of injury to one of the active phases. The reliability of present-day apparatus has reduced the emergency value of the additional phase, so some machines have been manufactured leaving out the third phase with consequent savings in cost.

Analysis of the Single-Phase Synchronous Machine

Basic assumptions underlying the general treatment already have been stated. The machines considered are of the salient-pole type having axes of symmetry at the pole centers and also midway between the poles, thus giving rise to direct and quadrature components of current, voltage, flux, and reactance. Further, the armature winding is assumed to be sinusoidally distributed, thereby fulfilling the conditions for the "ideal" synchronous machine. It is to be understood that the magnitudes of all quantities are expressed in per-unit terms.

The method of symmetrical components can be used to analyze the single-phase machine and to obtain correct results when harmonic voltages and currents are neglected. However, with different impedances in the two axes, a more detailed analysis appears necessary. The flux-linkage equations previously developed[1] for the three-phase machine completely define the operation of that type, and since the same equations are immediately adaptable to the single-phase machine the treatment here is based on them.

Vector Diagram—Excitation and Torque-Angle Characteristics

The analysis by way of flux linkages is carried through in Appendix 1 and is based upon the general two-pole machine shown in Figure 1. From equation 21 of that development one may draw the vector diagram for the single-phase machine, Figure 2. The diagram includes armature resistance, although it often can be neglected because it is small compared with the synchronous reactances. The third-harmonic voltage of equation 21 is not included in the diagram. Figure 2 thus corresponds exactly to that deduced by the method of symmetrical components.

The vector diagram represents a generator supplying a lagging current. All quantities have been divided by $\sqrt{3}$, the terminal voltage, e, being measured from line to neutral. It should be pointed out that the voltage drops are induced quantities, as shown by the direction of the arrows, and conform to equation 21 from which the figure is obtained. When treated as components of the terminal voltage the arrows on the resistance and reactance drops would be reversed. It is in this latter manner that they are usually regarded.

For the purpose of calculating the excitation, the vector diagram corresponding to cylindrical-rotor construction may be used. With usual values of voltage, current, power factor, and machine constants,[2] this form of diagram gives an excitation very close to that obtained when considering the actual effects of the salient poles.

Figure 3 shows the excitation diagram for the single-phase machine in terms of that for an equivalent cylindrical rotor, the construction of the diagram being similar in steps to that for the three-phase machine. Point m divides the total synchronous reactance drop, ea, which is $i\,(x_d+x_2)$ for the single-phase cylindrical-rotor machine, in the ratio $(x_q+x_2)/(x_d+x_2)$.[3] The projection of the excitation vector, oa, on the line om extended gives the excitation ob for the salient-pole machine.

Using the particular machine constants accompanying Figure 3, $oa=1.89$ and $ob=1.87$. With unity power factor, the excitation for the same machine as a cylindrical-rotor generator is 1.45 as against 1.41 from the salient-pole diagram. At leading power factors the difference becomes greater, being about ten per cent at 0.707 power factor leading.

From the preceding voltage expressions of Appendix 1, or perhaps better from the vector diagram of Figure 2

$$\begin{aligned} e_d &= e\cos\delta + 2i_{q1}r + i_{d1}(x_d+x_2) \\ e_q &= -e\sin\delta - 2i_{d1}r + i_{q1}(x_q+x_2) \end{aligned} \qquad (1)$$

Neglecting armature resistance, these quantities may be solved simultaneously for the currents which are

$$i_{d1} = \frac{e_d - e\cos\delta}{x_d+x_2}$$

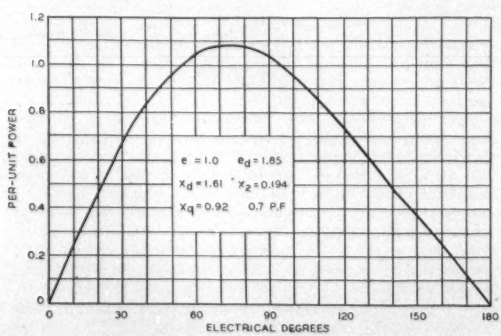
and

$$i_{q1} = \frac{e_q + e\sin\delta}{x_q+x_2} \qquad (2)$$

The power output of the machine is

$$P = \frac{ee_d}{x_d+x_2}\sin\delta + \frac{ee_q}{x_q+x_2}\cos\delta + \frac{e^2(x_d-x_q)}{2(x_d+x_2)(x_q+x_2)}\sin 2\delta \qquad (3)$$

Figure 4. Power-angle characteristics of a single-phase motor



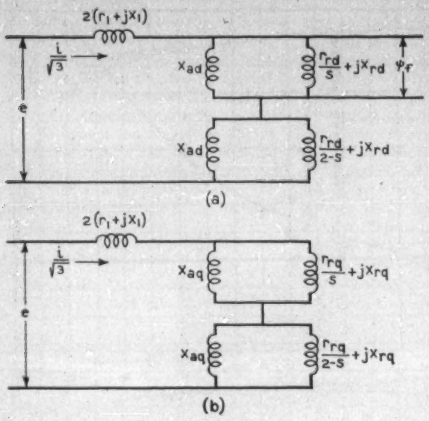


Figure 5. Equivalent circuits of a single-phase synchronous machine with one rotor circuit in each axis

a. Direct-axis circuit
b. Quadrature-axis circuit

Under steady-state operation e_q is zero, hence only the first and last terms of the foregoing power equation become significant. The procedure here is that shown by Doherty and Nickle.[3] As pointed out by them, the first term of the equation is that which would be obtained by cylindrical-rotor theory. The third term of the preceding equation, however, which signifies reluctance power, is due entirely to the salient-pole construction of the machine and is independent of any excitation on the rotor.

Figure 4 shows the power-angle curve for a typical single-phase salient-pole machine. The ordinate scale is power expressed in per unit of the three-phase rating, and the abscissa is the angle δ measured in electrical degrees. The machine is the same one discussed in reference to Figure 3, the constants of which are shown on the diagram. Maximum power is obtained from the preceding general equation by setting $dP/d\delta=0$, solving for δ and then substituting it in the power expression.

Figure 4 has been obtained from the equation

$$P=1.04 \sin \delta+0.172 \sin 2\delta \qquad (4)$$

with $e_d=1.87$ and $e=1.0$. It will be observed that the second term in the power equation is significant in magnitude, and affects considerably the pull-out angle of the machine although it increases the pull-out power only about five per cent. For the constants given the pull-out angle is approximately 74 electrical degrees.

The machine from which the constants were taken is rated 21,500-kva single phase, the current corresponding to 37,200-kva 3-phase. Normal power output is 15,000-kw at 0.7 power factor, and the normal torque angle therefore is 17.2 electrical degrees. Using the 3-phase base, the pull-out power is 2.68 times the normal single-phase load.

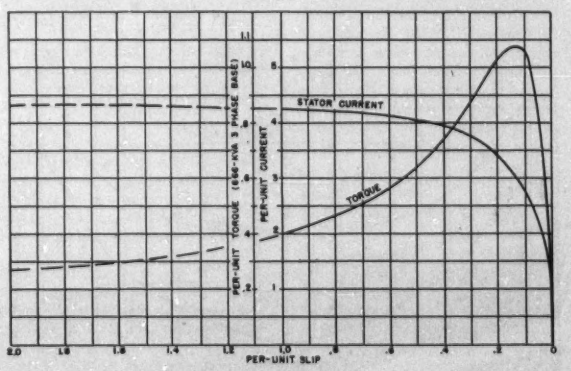
The load angle, δ, in terms of the load and the machine constants can be expressed by the relation

$$\delta=\tan^{-1}\frac{i(x_q+x_2)\cos\phi}{\sqrt{3}e+i(x_q+x_2)\sin\phi} \qquad (5)$$

Figure 2 is useful in the derivation. Neglecting armature resistance in load-angle analyses for very large machines introduces an error generally of less than one per cent, but resistance may not usually be disregarded when treating with small machines.

Figure 6. Three-phase acceleration current and torque curves of 500-kva 750-rpm 2,300-volt 25-cycle single-phase synchronous motor. Field short-circuited through discharge resistor

Data taken at reduced voltage and referred to normal. Dotted portion of curve to left of slip = 1 calculated from equivalent circuit



Sudden Change of Load

It is of usual occurrence that a machine operating under steady load conditions and corresponding rotor displacement suddenly undergoes a change in load. Such action is equivalent to shifting the rotor suddenly to a different angle. The load may be increased or decreased, but it is generally of more interest to know the machine performance under the first condition because of the importance of the new position with respect to breakdown torque.

When the machine finally has come back to steady-state operation it is of course also back on its steady-state power-angle curve, excitation and terminal voltage remaining constant. During the transient period, however, the machine follows a power-angle curve which is different from that of steady-state operation, and the torque at the new displacement angle will therefore be different from what it would be at the same angle under steady-state conditions.

For any given machine there is obviously a power-angle characteristic for each value of excitation and for each value of terminal voltage. This is shown by previous equations. From any one of these steady-state characteristics there will be any number of deviations to new power-angle relations, the deviation depending upon the amount of sudden load change.

The analysis for such conditions of operation for the three-phase machine has been given by Doherty and Nickle[4] and by Park.[5] For the single-phase machine

it remains merely to follow the same general treatment, remembering that the reactances now are modified by the addition of the negative-sequence reactance, x_2, as indicated in the development in this paper. If these substitutions of reactance are made, the value of synchronizing torque, T_{s1}, for sudden load change, with the machine running at an initial angle δ', is found to be

$$T_{s1}=\frac{ee_d'}{x_d+x_2}\cos\delta'+e^2\frac{(x_d-x_q)}{(x_d+x_2)(x_q+x_2)}\times\cos 2\delta'+e^2\frac{(x_d-x_d'')}{(x_d''+x_2)(x_d+x_2)}\sin^2\delta'+e^2\frac{(x_q-x_q'')}{(x_q+x_2)(x_q''+x_2)}\cos^2\delta' \quad (6)$$

The first two foregoing terms are seen to be the synchronizing torque at the angle δ' under steady-state operation, that is, the slope of the steady-state torque-angle curve.

Change of Circuit

It may happen that a three-phase machine has one of its lines opened so that it must care for its load by single-phase operation. With the machine terminal voltage, excitation, and load remaining constant, it is of interest to know the effect upon power angle, power factor, and line current due to the change in machine circuit. It is, of course, evident that the case of constant load is only one of several conditions of operation that might be imposed upon the machine, but since it is the usual case met in practice it is the one discussed here.

The steady-state power angle can be obtained easily for both the three-phase and the single-phase machine from the power-angle equations. The first has been presented previously,[3] and that for the single-phase machine is given in this paper. The excitation, e_d, is computed for the three-phase machine, assuming a cylindrical rotor, for a given power factor. It remains constant.

Knowing, then, the value of δ, its trigonometric functions can be substituted in the power-factor relation for the single-phase machine and the new power factor obtained. The ratio of the inphase component of current to the total current is the power factor, from Figure 2, resistance neglected,

$$\text{power factor}=\frac{i_{d1}\sin\delta+i_{q1}\cos\delta}{i/\sqrt{3}}=\frac{\frac{e_d-e\cos\delta}{(x_d+x_2)}\sin\delta+\frac{e\sin\delta}{(x_q+x_2)}\cos\delta}{\sqrt{\left(\frac{e_d-e\cos\delta}{(x_d+x_2)}\right)^2+\left(\frac{e\sin\delta}{(x_q+x_2)}\right)^2}} \quad (7)$$

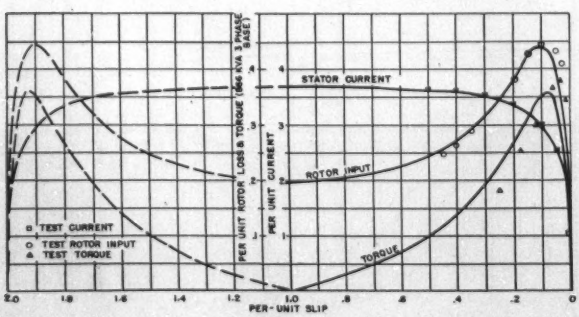


Figure 7. Calculated single-phase acceleration current, rotor input, and torque

Curves of 500-kva 2,300-volt 25-cycle single-phase synchronous motor, field short-circuited through discharge resistor. Test points indicated by dots

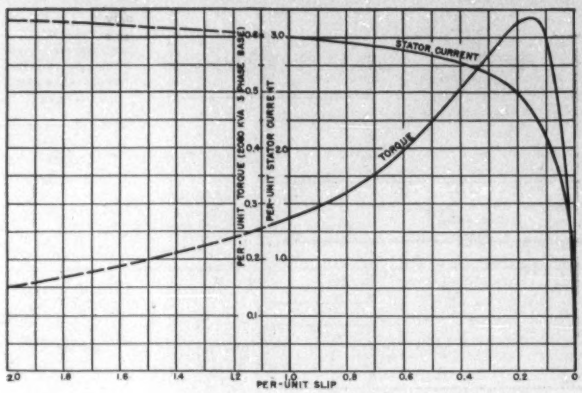


Figure 8. Three-phase acceleration current and torque curves of 1,200-kva 750-rpm 2,300-volt single-phase synchronous motor

Field short-circuited through discharge resistor. Data taken at reduced voltage and referred to normal. Dotted portion of curve to left of slip = 1 calculated from equivalent circuit

With the power, power factor, and voltage now known, the single-phase line current follows immediately.

The power-angle relations for the machine previously mentioned are, single phase and three phase, respectively,

$$P_1=1.04\sin\delta+0.172\sin 2\delta,\ e_d=1.87 \quad (8)$$
$$P_3=1.17\sin\delta+0.233\sin 2\delta,\ e_d=1.87$$

For a power output of 15,000 kw at 0.7-power factor lagging (0.405 per-unit power)

$\delta_1=17.2$ electrical degrees
$\delta_3=14.5$ electrical degrees

respectively, and the power factors are

$pf_1=0.7$
$pf_3=0.645$

respectively.

A second illustration is furnished by a general-purpose three-phase motor having constants $x_d=0.8$, $x_q=0.44$, $x_2=0.15$, $e=1$, and 0.8 load. The machine was assumed to be running under three-phase conditions when one lead was opened, and the steady-state values obtained are given in Table I.

It will be noted that the single-phase power factors do not vary to any great extent from those of the three-phase cases. This is accounted for by the fact that the negative-sequence reactance is the only quantity causing any difference between

the power-factor expressions, and, in general, x_2 is not very large. The positive-sequence component of the single-phase current will therefore be approximately equal in magnitude to the three-phase line current. It can thus be said that, in general, the line current will rise to a value close to 1.73 times the initial line current when a three-phase machine has one lead opened while operating with constant load, excitation, and terminal voltage.

Starting Performance

The starting performance of a single-phase machine is calculated by means of the equivalent circuits representing the direct and quadrature axes, as has been presented [5,6] for the three-phase machine, except that the circuits are more involved. The equivalent circuits must satisfy the following conditions as set forth in equation 21: First, at standstill and with $\sqrt{3}e$ applied line-to-line, the current that flows is determined by the reactances $2x_d''/\sqrt{3}$ for the direct axis, and $2x_q''/\sqrt{3}$ for the quadrature axis. Second, at synchronism these reactances become $(x_d+x_2)/\sqrt{3}$ and $(x_q+x_2)/\sqrt{3}$, respectively.

From physical reasoning the following conditions must be satisfied. First, at any speed the armature copper loss is two-thirds of that due to the same current for 3-phase operation. Second, the impedance must be the same at the same positive or negative speed. Third, the currents in the rotor circuits must consist of two components, one at slip frequency, sf, and the other at the frequency $(2-s)f$. Fourth, the rotor currents are proportional to $i/\sqrt{3}$, whereas in the 3-phase machine they are proportional to i. Fifth, the starting torque is zero at standstill. In a machine having unequal impedances in the two major axes, the statements of the third and fourth conditions are not strictly complete since there are forward- and backward-moving magnetomotive forces of the rotor currents due to both the positive- and negative-sequence armature currents. The conditions involve the assumption, which is realized quite closely in single-phase machines except at small values of slip, that the backward-moving fields of the positive- and negative-sequence stator currents are zero.

The equivalent circuits that must satisfy the several conditions noted herein for the direct and quadrature axes of the single-phase machine with one rotor circuit only in each axis are shown in Figure 5. The line current is i, and $i/\sqrt{3}$ is the magnitude of the forward- and backward-moving current systems given in equation 12 of Appendix I. The rotor resistance and reactance are r_r and x_r, respectively. The analysis of starting performance from the equivalent circuits is given in Appendix II.

To determine the accuracy of the theory, test data were taken on two different single-phase motors with direct-connected d-c generators. The test data consisted of acceleration-torque information both single and 3-phase, open- and short-circuit core-loss measurements, and windage and friction curves. The 3-phase torque and current data are shown plotted in Figures 6 and 8. The projections to the left of the $s=1$ axis were calculated from test results for equivalent circuits that checked the tests at slips less than unity. Figures 7 and 9 shows the calculated and test curves for single-phase acceleration. The calculated curves were obtained from the relations that $s(z_{AD}+z_{AQ})/2$ holds for current and torque at slip s, and $(2-s)(z_{AD}+z_{AQ})/2$ holds for current and torque at slip $2-s$.

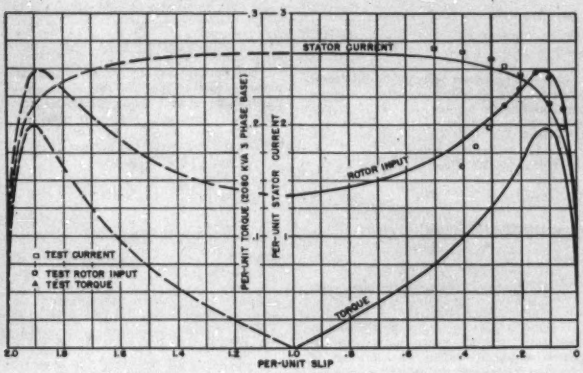


Figure 9. Calculated single-phase acceleration current, rotor input, and torque curves of 1,200-kva 750-rpm 2,300-volt 25-cycle single-phase synchronous motor, field short-circuited through discharge resistor

Test points indicated by dots

Load Loss

Under single-phase operation the load loss may be several times that under 3-phase operation with the same power output. This is due to the backward-rotating field, and so can be determined from it. If the squirrel-cage resistance is large, most of the load loss then will be in it. To give a definite check on the theory when iron or other stray losses are negligible, tests were made on an 8,250-kva 360-rpm 60-cycle motor with amortisseur bars having a resistance 25 times that of copper. The machine was driven at rated speed with backward rotation of the air-gap flux produced by a three-phase applied voltage. Under these conditions the input to the driving motor in excess of the windage, friction, and other constant losses equals the input to the set. Single-phase short-circuit core-loss tests also were taken. The results are shown in Figure 10.

Relations When Harmonic Currents Flow

The preceding analyses are based upon the assumption that only fundamental currents and voltages exist. This is fulfilled in machines designed especially for single-phase operation. However, in cases of unbalanced operation of some 3-phase machines, and in testing them for negative-sequence reactance, harmonics may appear. It is desirable to know the magnitude of these harmonics.

The currents under sustained single-phase short-circuit conditions can be written in the form

$$i_a = i_n \cos nt$$

$$= \sum \frac{i_n}{\sqrt{3}}[\cos(nt-30)+\cos(nt+30)] \quad (9)$$

Corresponding equations for the currents i_b and i_c would follow as in Appendix I. The first term in the brackets is the positive-sequence component and the second is the negative-sequence component. However, with the exception of the case in which $n=1$, the currents all will be

varying with respect to the rotor. A marked simplification results then by assuming that the reactance to nonfundamental currents is the same for a positively-rotating system as for one rotating negatively. The fundamental components when $n=1$ will meet synchronous and subtransient reactances according to whether they are of positive-sequence or negative-sequence character. The values of i_n are in geometric ratio when resistance is neglected, and the results apply for an external reactance as well as for a short circuit by making the appropriate allowance in the internal reactances. The analysis is similar to that for fundamental currents only, except that each current harmonic produces three voltages, one of its own harmonic order, one two orders lower, and one two orders higher. Thus the third-harmonic current produces fundamental, third-, and fifth-harmonic components in the terminal voltage.

The maximum ratio between harmonics is $(\sqrt{x_d''}-\sqrt{x_d''})/(\sqrt{x_q''}+\sqrt{x_d''})$. If $x_d''=x_q''$, the ratio is, of course, zero. For machines with x_d'' different from x_q'' by not more than 15 per cent, the harmonics are negligible for usual analyses.

Appendix I. Steady-State Voltage Equation With Fundamental Currents Flowing

The 2-pole synchronous machine is shown in Figure 1. Based on it the expression for the per-unit flux linkages of phase a of an ideal 3-phase machine has been given[1] by

$$\psi_a = e_d \cos\theta - e_q \sin\theta - \frac{x_0}{3}(i_a+i_b+i_c) - \frac{x_d+x_q}{3}\left(i_a-\frac{i_b+i_c}{2}\right) - \frac{x_d-x_q}{3}[i_a\cos 2\theta + i_b\cos(2\theta-120) + i_c\cos(2\theta+120)] \quad (10)$$

The corresponding expressions for the linkages of phases b and c are stated in a similar manner. Only positive-sequence currents are considered.

The single-phase machine, due to its method of construction, may be treated as a 3-phase machine with one phase left idle in operation, hence the single-phase armature currents are

$$i_a = i\cos t$$
$$i_b = -i_a = -i\cos t \quad (11)$$
$$i_c = 0$$

These currents can be resolved into their positive- and negative-sequence components, and hence become

$$i_a = i\cos t = \frac{i}{\sqrt{3}}[\cos(t-30)+\cos(t+30)]$$
$$i_b = -i\cos t = \frac{i}{\sqrt{3}}[\cos(t-150)+\cos(t+150)]$$
$$i_c = 0 = \frac{i}{\sqrt{3}}[\cos(t+90)+\cos(t-90)] \quad (12)$$

The first trigonometric function of each of these three current expressions form the positive-sequence system, the last set of three terms forming the negative-sequence group. The positive-sequence components of current meet with the synchronous reactances when the rotor is moving normally, and the negative-sequence components meet with the subtransient reactances which are to be denoted by double primes. The appropriate factor is therefore to be used with each type of current.

Substituting the last three relations for the single-phase currents in the linkage expression previously given, the single-phase flux linkages for phase a are

$$\psi_a = e_d\cos\theta - e_q\sin\theta - \frac{i}{\sqrt{3}}\left[\frac{x_d+x_q}{2}\times\cos(t-30)+\frac{x_d-x_q}{2}\cos(t+2\theta_0+30)+\frac{x_d''+x_q''}{2}\cos(t+30)+\frac{x_d''-x_q''}{2}\cos(3t+2\theta_0+30)\right] \quad (13)$$

The flux-linkage equations for phases b and c are obtained similarly.

The resultant voltage of each armature phase is equal to the time rate of change of flux linkages of that circuit minus the armature resistance drop. Hence, for phase a,

$$e_a = p\psi_a - ir = -e_q\cos\theta - e_d\sin\theta + \frac{i}{\sqrt{3}}\left[\frac{x_d+x_q}{2}\sin(t-30)+\frac{x_d-x_q}{2}\sin(t+2\theta_0+30)+x_2\sin(t+30)+3\left(\frac{x_d''-x_q''}{2}\right)\times\sin(3t+2\theta_0+30)\right]-ir\cos t \quad (14)$$

It will be noticed that x_2 has replaced the term $(x_d''+x_q'')/2$ which is the negative-sequence reactance of the machine when connected to a system through a large external impedance. The resistance drop for phase b becomes a positive quantity because $i_b=-i_a=-i$ in magnitude. The resistance drop for phase c is of course zero.

The line-to-line voltage under consideration is the difference between the voltages of phases a and b, hence

$$e_a-e_b = -\sqrt{3}e_d\sin(\theta+30)-\sqrt{3}e_q\cos(\theta+30)+i\left[\frac{x_d+x_q}{2}\sin t+\frac{x_d-x_q}{2}\sin(t+2\theta_0+60)+x_2\sin t+3\left(\frac{x_d''-x_q''}{2}\right)\sin(3t+2\theta_0+60)\right]-2ir\cos t$$
$$= -\sqrt{3}e_d\sin(\theta+30)-\sqrt{3}e_q\times\cos(\theta+30)+i\Big[x_d\sin(\theta+30)\times\cos(\theta_0+30)-x_q\cos(\theta+30)\times\sin(\theta_0+30)+x_2\sin(\theta+30)\cos(\theta_0+30)-x_2\cos(\theta+30)\sin(\theta_0+30)+3\left(\frac{x_d''-x_q''}{2}\right)\sin(3t+2\theta_0+60)-2r\cos(\theta+30)\cos(\theta_0+30)-2r\times\sin(\theta+30)\sin(\theta_0+30)\Big] \quad (15)$$

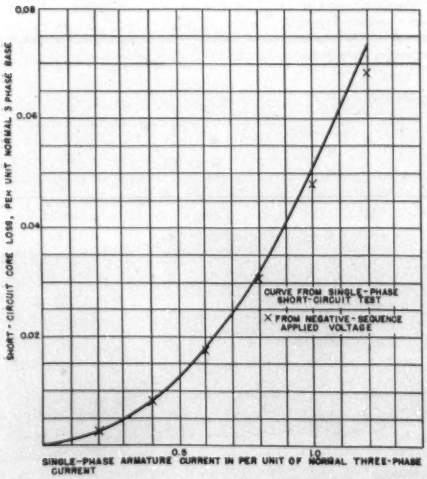


Figure 10. Single-phase armature current in per unit of normal 3-phase current

Thus far the armature currents have been expressed in terms of i, but now it is desired to express them in terms of their direct and quadrature positive- and negative-sequence components. The direct component, i_d, of the positive-sequence current for a three-phase machine is

$$i_d=\frac{2}{3}[i_a\cos\theta+i_b\cos(\theta-120)+i_c\cos(\theta+120)] \quad (16)$$

Considering only the direct positive-sequence components of the single-phase currents

$$i_{d1}=\frac{2}{3}\frac{i}{\sqrt{3}}[\cos(t-30)\cos\theta+\cos(t-150)\cos(\theta-120)+\cos(t+90)\cos(\theta+120)]$$

$$=\frac{i}{\sqrt{3}}\cos(\theta_0+30) \quad (17)$$

Also, for the quadrature positive-sequence components of current

$$i_{q1}=-\frac{i}{\sqrt{3}}\sin(\theta_0+30) \quad (18)$$

The negative-sequence components are

$$i_{d2}=\frac{i}{\sqrt{3}}\cos(\theta_0-30) \quad (19)$$

and

$$i_{q2}=\frac{-i}{\sqrt{3}}\sin(\theta_0-30) \quad (20)$$

Substituting these relations for the current components in the line-to-line voltage equation, there is obtained

$$e_a-e_b=\sqrt{3}\Big[-e_d\sin(\theta+30)-e_q\cos(\theta+30)+i_{d1}(x_d+x_s)\times\sin(\theta+30)+i_{q1}(x_q+x_s)\cos(\theta+30)+\sqrt{3}i\left(\frac{x_d''-x_q''}{2}\right)\sin(3t+2\theta_0+60)-2i_{d1}r\cos(\theta+30)+2i_{q1}r\sin(\theta+30)\Big]$$

$$=\sqrt{3}e\cos(t+\phi) \quad (21)$$

where ϕ is the power-factor angle of the machine. Similar equations for (e_b-e_c) can be written by inspection.

Table I

Power Angle	Power Factor	Line Current	Phase
24.0	0.8 lagging	1.0	3
35.0	0.824 lagging	1.68	1
19.0	1.0	1.0	3
26.0	1	1.73	1
15.5	0.8 leading	1.0	3
20.4	0.868 leading	1.6	1

Appendix II. Starting Performance From Equivalent Circuits

The solution of the equivalent circuits for for the direct and quadrature axes of the single-phase machine give impedances of the form

$$Z_D=Z_{AD}+2(r_1+jx_1)$$

and (22)

$$Z_Q=Z_{AQ}+2(r_1+jx_1)$$

in which Z_{AD} and Z_{AQ} both contain terms for the two series rotor circuits. From Figure 5,

$$Z_D=2(r_1+jx_1)+\frac{jx_{ad}\left(\frac{r_{rd}}{s}+jx_{rd}\right)}{j(x_{ad}+x_{rd})+\frac{r_{rd}}{s}}+\frac{jx_{ad}\left(\frac{r_{rd}}{2-s}+jx_{rd}\right)}{j(x_{ad}+x_{rd})+\frac{r_{rd}}{2-s}} \quad (23)$$

$$Z_Q=2(r_1+jx_1)+\frac{jx_{aq}\left(\frac{r_{rq}}{s}+jx_{rq}\right)}{j(x_{aq}+x_{rq})+\frac{r_{rq}}{s}}+\frac{jx_{aq}\left(\frac{r_{rq}}{2-s}+jx_{rq}\right)}{i(x_{aq}+x_{rq})+\frac{r_{rq}}{2-s}}$$

The starting current is given very closely by the expression

$$i=\frac{2}{Z_D+Z_Q}\sqrt{3}e \quad (24)$$

where $\sqrt{3}e$ is the line-to-line voltage.

The rotor input is the real part of $i/\sqrt{3}$ less the stator loss. The net developed torque is the difference between the rotor input from the forward-rotating field and that from the backward-rotating field, and is

$$T=\text{real part of}\left(\frac{i}{\sqrt{3}}\right)^2\left[s\left(\frac{Z_{AD}+Z_{AQ}}{2}\right)-(2-s)\left(\frac{Z_{AD}+Z_{AQ}}{2}\right)\right] \quad (25)$$

where $(i/\sqrt{3})^2$ is the square of the *magnitude* of $i/\sqrt{3}$.

In Figure 5 the real part of $s(Z_{AD}+Z_{AQ})/2$ is

$$\frac{1}{2}\frac{\frac{r_{rd}}{s}x_{ad}^2}{(x_{ad}x_1+x_dx_{rd})^2+x_d^2\left(\frac{r_{rd}}{s}\right)^2}+\frac{1}{2}\frac{\frac{r_{rq}}{s}x_{aq}^2}{(x_{aq}x_1+x\,x_{rq})^2+x_q^2\left(\frac{r_{rq}}{s}\right)^2} \quad (26)$$

and the real part of $(2-s)(Z_{AD}+Z_{AQ})/2$ is

$$\frac{1}{2}\frac{\frac{r_{rd}x_{ad}^2}{2-s}}{(x_{ad}x_1+x_dx_{rd})^2+x_d^2\left(\frac{r_{rd}}{2-s}\right)^2}+\frac{1}{2}\frac{\frac{r_{rq}x_{aq}^2}{2-s}}{(x_{aq}x_1+x_qx_{rq})^2+x_q^2\left(\frac{r_{rq}}{2-s}\right)^2} \quad (27)$$

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No Discussion

Performance of the Single-Phase Synchronous Machine

B. L. ROBERTSON
FELLOW AIEE

T. A. ROGERS
MEMBER AIEE

THE THEORY of operation of the single-phase synchronous machine is given by Terry and Robertson.[1] Because of its most usual manner of construction, the single-phase machine is treated as a 3-phase machine with direct and quadrature axes of symmetry and with one phase winding left idle. Of particular interest to that paper is the general analysis of the 3-phase machine as a single-phase motor or generator under steady-state operation, and the development of the expression for the line-to-line voltage with the modification of the component impedance drops as they appear normally in the treatment for the 3-phase machine.

This paper presents test data on the steady-state performance of that type of single-phase synchronous machine under both generator and motor action and for different values of power factor. They are given to substantiate the theoretical development, and in so doing show an extremely close comparison between calculation and test for complete load runs rather than for particularly chosen points. To the authors' knowledge such test data have not been presented previously. In addition, the paper shows that the harmonics in the single-phase machine practically may be neglected in the steady-state performance as given by the voltage vector diagram.

Experimental Results

Test results of torque angle and excitation under steady-state single-phase operation, and the direct comparison with calculations, for a series of load runs taken on a synchronous machine are given in Table I. The machine is a salient-pole synchronous motor with a 3-phase rating of 40-horsepower 440-volts 44.5-amperes 1,200 revolutions per minute 60-cycles. Impedance constants for the machine,[2] obtained from test measurements, are

$x_d = 1.04,\ x_q = 0.565,\ x_2 = 0.211$

and

$r_a = 0.093$, all in 3-phase per-unit terms.

The magnetization curve for the machine starts to depart from the air-gap line at about 180 volts, and at normal voltage the machine has a very high degree of saturation. Practically, to exclude attendant effects of saturation, the load runs were taken at approximately half voltage.

As observed from the data, the agreement between calculation and test is very good, particularly so for the machine operating as a motor at unity power factor and at 0.8 power factor lagging. The greatest difference shown in the table is less than five per cent and is for the machine under generator action at unity power factor. Making corrections for saturation, however, any discrepancies between calculation and test are removed and the agreement becomes practically exact.

The Vector Diagram

The vector diagram, which portrays the steady-state performance of the machine, and which in itself is the graphical solution, corresponds to the equation developed by Terry and Robertson[1] for the voltage between terminals a and b of a 3-phase machine carrying a single-phase load. Revising their equation slightly to show excitation only in the direct axis, neglecting the third-harmonic component of voltage, and expressing all voltage elements as components of the terminal voltage, we have

$$\begin{aligned} e_a - e_b = \sqrt{3}[&e_d \sin(\theta+30) - \\ &i_d(x_d+x_2)\sin(\theta+30) - \\ &i_q(x_q+x_2)\cos(\theta+30) + \\ &2i_d r\cos(\theta+30) - 2i_q r\sin(\theta+30)] \\ &= \sqrt{3}e_t\cos(t+\phi) \quad (1) \end{aligned}$$

in which

e_d = direct-axis excitation voltage per phase
e_t = terminal voltage per phase
i_d and i_q = direct- and quadrature-axis components of the positive-sequence line current i (i is 57.7 per cent of actual line current)
x_d and x_q = 3-phase direct- and quadrature-axis synchronous reactances
x_2 = negative-sequence reactance
r = armature resistance per phase
θ = angle between direct axis and axis of phase
ϕ = power factor angle

In complex notation, and for the single-phase motor with lagging power factor, equation (1) becomes

$$e_t = e_d + i_d(x_d+x_2) + ji_q(x_q+x_2) + 2i_q r - 2ji_d r \quad (2)$$

The excitation voltage, e_d, is taken as the reference vector, whence

$$i = i_q - ji_d \quad (3)$$

Because terminal conditions are given practically by the line voltage, the line current, and the power factor, it is necessary to start from these data and to locate the direct axis from them.[3] Excitation, torque angle, and the current components then are shown explicitly.

Method of Calculation

The method of calculation of the quantities given in Table I may be understood through the aid of Figure 1 which shows the voltage vector diagram for the single-phase machine. To illustrate the proce-

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B. L. ROBERTSON is professor of electrical engineering, University of California, Berkeley, Calif., and T. A. ROGERS is associate professor in the department of engineering, University of California, Los Angeles, Calif.

dure one set of calculations is outlined, that for the machine under motor action at 0.79 power factor lagging and with 30 amperes line current. Figure 1 was drawn for this specific case.

The terminal voltage of 0.533 (all quantities are expressed in per-unit terms) is laid off as the reference vector. The line current of 30 amperes, divided by the root of three and expressed as the decimal fraction 0.389 of normal current, is drawn from the origin at the proper power-factor angle. Next is laid off the resistance drop of 0.072, followed by the vector

$$i(x_a+x_2)=0.302$$

which locates the direct axis. The torque angle is immediately read as 33.7 degrees, comparing with 33.5 degrees as found by test. Since both components of current meet the same resistance it is not necessary to know components of the total ir drop.

With the direct axis located, the positive-sequence component of the line current is

$$i=0.388-j0.027$$

and the corresponding reactance drops are 0.301 and -0.034 per unit. The armature current is magnetizing, as always is the case under motor operation when the torque angle leads the power-factor angle. The reactance drops shown in Figure 1 determine the length of the excitation voltage, e_d, which also can be obtained from the normal projection of the tip of the vector

$$i(x_d+x_2)$$

upon the direct axis. Knowing that rated voltage, when carried to the air-gap line, requires 3.6 amperes field current, the excitation voltage of 0.34 calls for 1.23 amperes. This compares quite well with 1.25 amperes field current read during tests.

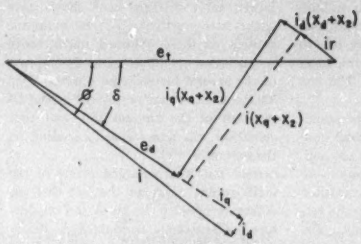

Figure 1 (above). Vector diagram for the salient-pole single-phase synchronous machine

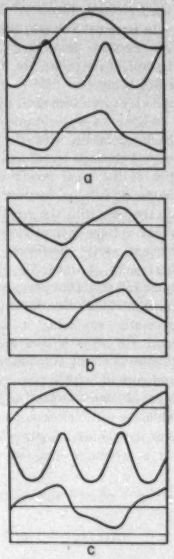

Figure 2 (right). Wave shapes of line voltage, field current, and armature current of a single-phase machine

(a). Machine connected to an infinite bus 200-volts 39.6-amperes 7.76-kw 0.98 power factor

(b). Machine supplying a resistance load 200-volts 38.4-amperes 7.72-kw

(c). Machine supplying a resistance-capacity load. 200-volts 43.2-amperes 7.76-kw 0.89 power factor

Although armature resistance usually is neglected in synchronous machine studies, there are, nevertheless, many instances in which it has a pronounced effect and should be taken into account. This is particularly true for small machines. The effective armature resistance of the machine used in these tests is 0.054 per phase corresponding to rated line current. Because twice this phase resistance enters calculations, the resistance drop at normal current is 10.8 per cent of rated voltage and therefore is appreciable in magnitude. To neglect it would not allow the close agreement between calculated and test results which are given in the table.

Since the vector diagram does not take into account the constant losses, the reference for zero torque angle was determined at that condition of loading under which there was no electrical input to the armature of the a-c machine, all power for the losses being supplied by the d-c driver.

Table I. Torque Angle and Excitation of a Single-Phase Synchronous Machine

40-Horsepower 440-Volts 44.5-Amperes 1,200-Revolutions Per Minute, Salient Pole, 3-Phase Rating

e_t, Volts	i, Amperes	W, Watts	Degrees Angle Calculated	Degrees Angle Test	Excitation Amperes Calculated	Excitation Amperes Test
			Unity Power-Factor Generator			
240	0	0	0	0	2.00	2.00
243	10.1	2445	10.0	10.0	2.15	2.16
246	19.9	4890	18.3	18.2	2.44	2.46
248.5	30.1	7470	25.5	25.0	2.83	2.88
249.5	39.5	9850	31.0	29.8	3.24	3.40
251.5	47.5	11940	34.8	33.5	3.62	3.77
			Unity Power-Factor Motor			
240	0	0	0	0	2.00	2.00
238	10.1	2405	11.3	11.5	1.94	1.93
237	20.0	4740	22.3	22.5	2.07	2.07
234.5	30.0	7035	33.2	33.5	2.34	2.33
232.5	40.0	9300	43.0	43.0	2.73	2.70
230	50.0	11500	51.2	51.0	3.21	3.20
			0.8 Power-Factor Lagging Motor			
241	0	0	0	0	2.01	2.01
239	10.1	1880	8.0	8.0	1.56	1.57
237.5	20.0	3800	19.0	19.5	1.32	1.33
235	30.0	5570	33.7	33.5	1.23	1.25
233	40.1	7390	51.6	51.5	1.41	1.44
			0.8 Power-Factor Leading Motor			
242	0	0	0	0	2.01	2.01
241	10.1	1945	9.3	9.5	2.35	2.36
240	20.0	3835	16.7	17.0	2.78	2.82
239	30.0	5840	23.5	23.5	3.21	3.32
237	40.0	7690	28.4	28.5	3.74	3.81
235	50.0	9420	32.3	32.5	4.28	4.33
233	60.0	11120	35.9	35.2	4.81	4.88

Harmonics

In order to show the character of the voltage and current wave shapes, and the harmonic content therein, several oscillograms were taken, the tracings of three of

them given by Figure 2. The curves indicate terminal voltage, field current, and line current, respectively, from top to bottom of the prints, the lowest line in all cases being zero field current. The test data are given with each tracing.

Figure 2*a* was obtained under pumpback operation with the machine connected through transformers to the supply of the local power company. All harmonic voltages become short-circuited at the bus, with the result that the terminal voltage is fundamental only. The current wave, however, contains odd harmonics of which the third is 21 per cent, and the fifth 4 per cent, of the fundamental. With the machine acting as a generator supplying a pure resistance load, the wave shapes are as given by Figure 2*b*. The third-harmonic voltage and current were approximately 13.5 per cent of their respective fundamentals, while the fifth-harmonic voltage and current were about 2.9 per cent. Figure 2*c*, for a generator load having a leading power factor of 90 per cent, shows wave shapes with practically the same harmonic content as those obtained under unity power factor operation. In only one case of the several investigated did the third harmonic in voltage or current exceed 25 per cent of the fundamental, and that condition was pure capacity loading on the generator. The Fourier analyses were carried out from enlarged prints of the oscillograms. Despite the fact that the difference between the direct and quadrature subtransient reactances is fairly great, and therefore accentuates the third harmonic, practically this magnitude is not such that it will affect appreciably the machine performance over that obtained by analysis treating with the fundamental alone. This conclusion is established by the data in the table.

Conclusions

The good agreement between calculation and test as shown here substantiates the theory of steady-state operation of the single-phase synchronous machine. Under the assumed condition of no saturation, the theory and actual performance check surprisingly well. The effect of armature resistance may not always be neglected without introducing serious error in both torque angle and excitation. Resistance was significant in the machine studied. It appears that the third-harmonic term which enters the line-to-line voltage equation for the single-phase machine has no effect upon steady-state performance and may be neglected with safety.

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3. Synchronous Machines I and II—An Extension of Blondel's Two-Reaction Theory, **R. E. Doherty, C. A. Nickle.** AIEE Transactions, volume 45, 1926, pages 912–47.

No Discussion

A Study of the 3-Phase Commutator Armature With Six Adjustable Brushes

PAUL W. FRANKLIN
ASSOCIATE AIEE

Synopsis: This paper attempts a detailed study of the characteristics of a three phase commutator armature with six adjustable brushes per pole pair. As the brushes are being shifted, the effective number of turns per phase, phase resistance, phase leakage reactance and flux shape, change considerably. These values are being derived as a function of the brush spacing angle and, with the help of the Goerges diagram, the resulting flux shapes are being determined. Furthermore, the change of leakage reactance with varying armature speed is being discussed. The principles of the derivations are general and may be applied to any similar problems.

CHANGES of the spacing angle between brushes of the same phase on a polyphase commutator machine cause a change of the number of effective turns per phase and thus a change in either speed or phase voltage. For his performance calculation, the designer has to know the following relations:

(*a*). Effective number of turns per phase versus brush spacing angle.

(*b*). Effective phase resistance versus brush spacing angle.

(*c*). Effective phase leakage reactance versus brush spacing angle.

(*d*). Resulting flux shape versus brush spacing angle.

(*e*). Change in leakage reactance versus varying armature speed.

In general, each phase brush current divides itself within the armature into two unequal components, these being determined by the resistance and reactance values of the two branches. For the case of the 3-phase machine, the component values are immaterial, since the conductor currents turn out to be straight fractions of the brush currents. This is due to the fact that the sum of the three positive brush currents is zero. For the case of a 2-phase machine with four brushes per pole pair, the conductor current is not independent of the current components and an accurate calculation becomes rather complicated.

For all further calculations the following fundamental assumptions are being made:

(*a*). The coil pitch shall be either 100 per cent or very close to it, so that the chord factor is unity. A coil pitch of less than 100 per cent causes even field harmonics which cannot be compensated and increase the leakage reactance of the windings.

(*b*). Brushes of the three phases shall be spaced symmetrically with regard to each other. A deviation from this again causes even field harmonics, which should be avoided.

(*c*). Brush currents are symmetrical.

(*d*). Saturation of the leakage path is neglected.

(*e*). Effects of the commutation cut in the conductor current are neglected.

Reference is made to the list of symbols given at the end of the paper.

Effective Turns Per Phase

Total number of turns per phase

$$W = \frac{z}{4c}\frac{2\alpha}{\pi} \tag{1}$$

Distribution factor for the nth harmonic

$$DF_n = \frac{\sin \alpha n}{\dfrac{N\alpha}{p\pi}\sin\left(\dfrac{p\pi n}{N}\right)} \tag{2}$$

and for a large number of slots N

$$DF_n = \frac{\sin \alpha n}{\pi n} \tag{3}$$

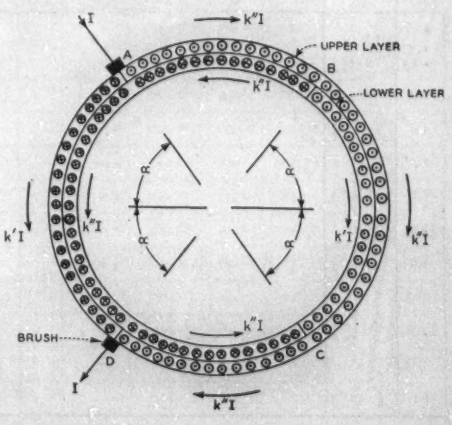

Figure 1. Current distribution in a single-phase armature

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PAUL W. FRANKLIN is with the engineering department, Eclipse-Pioneer Instrument Corporation, Teterboro, N. J.

therefore

$$W_{eff_n} = \frac{s}{2c}\,\frac{\sin \alpha n}{\pi n} \tag{4}$$

Current Distribution

The current distribution will be derived by adding together the current distributions of three single-phase systems, spaced symmetrically in time and space for 120 degrees. Considering a single phase system (Figure 1), each brush current is split into two branch conductor currents

$$I' = k'I \tag{4a}$$

$$I'' = k''I \tag{4b}$$

with k', k'' complex constants. The resulting distribution of the slot magnetomotive force is independent of the values of k', k'' and as follows:

Arc $\overline{AD}$ and $\overline{CB}$ ..... magnetomotive force per slot $= sI\sqrt{2}$

Arc $\overline{DC}$ and $\overline{BA}$ ..... magnetomotive force per slot $= 0$

Changing now to a 3-phase system, our three brush currents are

$$I_1 = I(k' + k'') \tag{5a}$$

$$I_2 = Ia(k' + k'') \tag{5b}$$

$$I_3 = Ia^2(k' + k'') \tag{5c}$$

We have to distinguish between three cases, namely,

$\alpha = 0$–30 degrees ..... Figure 2

$\alpha = 30$–60 degrees ..... Figure 3

$\alpha = 60$–90 degrees ..... Figure 4

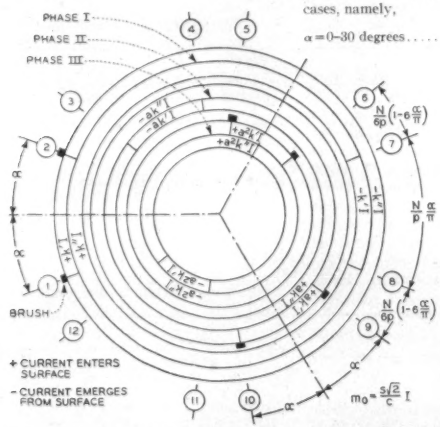

Figure 2. Current distribution, layer and slot magnetomotive forces in a 3-phase armature for $\alpha = 0$–30 degrees

In these figures, the current belt width, current per conductor, magnetomotive force per coil side, and resulting magnetomotive force per slot are shown and charted. The following conclusions may be drawn:

1. **For a symmetrical 3-phase system with a coil pitch of 100 per cent**

(a). **The absolute value of the current per conductor is independent of the branchc urrents and a portion of the brush current.**

(b). **The resulting magnetomotive force per slot is independent of the branch currents.**

2. For a 2-phase system or any system where the sum of the currents taken over all positive brushes is not equal to zero, we would have to state

(a). **The absolute value of the current per conductor is dependent upon the branch currents.**

(b). **The resulting magnetomotive force per slot is independent of the branch currents.**

The constant

$$m_0 = \frac{s\sqrt{2}}{c} I \tag{6}$$

is the maximum magnetomotive force per slot and used as a reference value. For our investigation of symmetrical 3-phase system, the conception of k' and k'' can be discarded from here on.

Effective Phase Resistance

We sum up the total copper losses in the armature with currents and belt widths as listed in the chart. This

Zone	No. of Slots	Upper Layer, Current of Phase 1	Upper Layer, Current of Phase 2	Upper Layer, Current of Phase 3	Total Current in Upper Layer	Total Mmf in Upper Layer	Lower Layer, Current of Phase 1	Lower Layer, Current of Phase 2	Lower Layer, Current of Phase 3	Total Current in Lower Layer	Total Mmf in Lower Layer	Total Mmf Per Slot
1–2	$\frac{N}{p}\frac{\alpha}{\pi}$	$k'I$	$-ak'I$	$-a^2k'I$	I	$\frac{s\sqrt{2}}{2c} I$	$k''I$	$ak''I$	$a^2k''I$	0	0	$\frac{m_0}{2}\,\underline{/0°}$
2–3	$\frac{N}{6p}\left(1-6\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$-a^2k''I$	0	0	$k''I$	$ak''I$	$a^2k''I$	0	0	0
3–4	$\frac{N}{p}\frac{\alpha}{\pi}$	$-k''I$	$-ak''I$	$-a^2k''I$	0	0	$k''I$	$-ak'I$	$a^2k''I$	$-aI$	$-\frac{sa\sqrt{2}}{2c}$	$\frac{m_0}{2}\,\underline{/-60°}$
4–5	$\frac{N}{6p}\left(1-6\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$-a^2k''I$	0	0	$k''I$	$ak''I$	$a^2k''I$	0	0	0
5–6	$\frac{N}{p}\frac{\alpha}{\pi}$	$-k''I$	$-ak''I$	$a^2k'I$	a^2I	$\frac{sa^2\sqrt{2}}{2c} I$	$k''I$	$ak''I$	$a^2k''I$	0	0	$\frac{m_0}{2}\,\underline{/-120°}$
6–7	$\frac{N}{6p}\left(1-6\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$-a^2k''I$	0	0	$k''I$	$ak''I$	$a^2k''I$	0	0	0
7–8	$\frac{N}{p}\frac{\alpha}{\pi}$	$-k''I$	$-ak''I$	$-a^2k''I$	0	0	$-k'I$	$ak''I$	$a^2k''I$	$-I$	$-\frac{s\sqrt{2}}{2c} I$	$\frac{m_0}{2}\,\underline{/+180°}$
8–9	$\frac{N}{6p}\left(1-6\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$-a^2k''I$	0	0	$k''I$	$ak''I$	$a^2k''I$	0	0	0
9–10	$\frac{N}{p}\frac{\alpha}{\pi}$	$-k''I$	$ak'I$	$-a^2k''I$	aI	$\frac{sa\sqrt{2}}{2c} I$	$k''I$	$ak''I$	$a^2k''I$	0	0	$\frac{m_0}{2}\,\underline{/+120°}$
10–11	$\frac{6p}{N}\left(1-\frac{\alpha}{6\pi}\right)$	$-k''I$	$-ak''I$	$-a^2k''I$	0	0	$k''I$	$ak''I$	$a^2k''I$	0	0	0
11–12	$\frac{N}{p}\frac{\alpha}{\pi}$	$-k''I$	$-ak''I$	$-a^2k''I$	0	0	$k''I$	$ak''I$	$-a^2k'I$	$-a^2I$	$-\frac{3sa^2\sqrt{2}}{2c} I$	$\frac{m_0}{2}\,\underline{/+60°}$
12–1	$\frac{N}{6p}\left(1-6\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$-a^2k''I$	0	0	$k''I$	$ak''I$	$a^2k''I$	0	0	0

copper loss is equivalent to $3I^2 R_a$ with R_a being the effective phase resistance. As a comparison value we use R_{dc} which is the resistance between two brushes spaced 180 electrical degrees apart, as used for d-c calculations. Then, for

$\alpha = 0$–30 degrees

$$R_\alpha = 4\frac{\alpha}{\pi}R_{dc} \tag{7a}$$

$\alpha = 30$–60 degrees

$$R_\alpha = 4\frac{\alpha}{\pi}R_{dc} \tag{7b}$$

$\alpha = 60$–90 degrees

$$R_\alpha = \frac{4}{3}\times R_{dc} \tag{7c}$$

This relation is plotted in Figure 5.

The Goerges Diagram and the Shape of the Air Gap Flux

The Goerges diagram, lately reintroduced by Liwschitz in one of his papers dealing with the differential leakage, is an extremely useful tool for the determination of the flux shape.

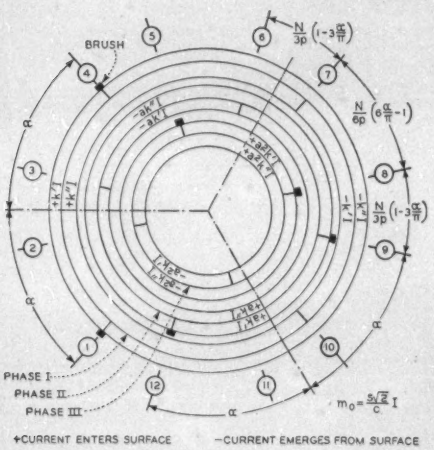

Figure 3. Current distribution, layer and slot magnetomotive forces in a 3-phase armature for $\alpha = 30$–60 degrees

If we represent each resulting slot magnetomotive force by its vector and, traveling around the armature periphery, add up all magnetomotive force vectors, we obtain a polygon which normally closes after each pole pair. The end points of these slot magnetomotive force vectors correspond to a tooth along the armature circumference, whereas the centers of these vectors correspond to the centers of slot openings. We draw a time reference line through the center of the polygon, which rotates in synchronism. It can be proved that the vertical projections from the polygon to this reference line are proportional to the resulting air gap magnetomotive force at the corresponding armature point at the time instant determined by the position of the reference line. Neglecting saturation in the iron, or estimating it, the air gap density now can be calculated and the flux shape determined.

The more this polygon approaches a circle, the more the flux shape will approach a sine curve, the less flux pulsations will occur, the less higher harmonics exist, and the smaller the differential leakage will be. It also may be seen that if the polygon is not symmetrical to any reference line, second harmonics with all their disadvantages will appear in the flux shape.

We will use the Goerges diagram for two purposes, first, to determine the flux shape, secondly, for the calculation of the belt leakage. To simplify our equations,

Zone	No. of Slots	Upper Layer Current of Phase 1	Upper Layer Current of Phase 2	Upper Layer Current of Phase 3	Total Current in Upper Layer	Total Mmf in Upper Layer	Lower Layer Current of Phase 1	Lower Layer Current of Phase 2	Lower Layer Current of Phase 3	Total Current in Lower Layer	Total Mmf in Lower Layer	Total Mmf Per Slot
1-2	$\frac{N}{6p}\left(6\frac{\alpha}{\pi}-1\right)$	$k'I$	$-ak''I$	$-a^{1/2}k'I$	I	$\frac{s\sqrt{2}}{2c}I$	$k''I$	$ak''I$	$-a^{1/2}k'I$	$-a^{1/2}$	$\frac{-sa^{1/2}\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0\angle 30°$
2-3	$\frac{N}{3p}\left(1-3\frac{\alpha}{\pi}\right)$	$k'I$	$-ak''I$	$-a^{1/2}k'I$	I	$\frac{s\sqrt{2}}{2c}I$	$k''I$	$ak''I$	$a^{1/2}k'I$	0	0	$\frac{m_0}{2}\angle 0°$
3-4	$\frac{N}{6p}\left(6\frac{\alpha}{\pi}-1\right)$	$k'I$	$-ak''I$	$-a^{1/2}k'I$	I	$\frac{s\sqrt{2}}{2c}I$	$k''I$	$-ak'I$	$a^{1/2}k''I$	$-aI$	$\frac{-sa\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0\angle -30°$
4-5	$\frac{N}{3p}\left(1-3\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$-a^{1/2}k'I$	0	0	$k''I$	$-ak'I$	$a^{1/2}k''I$	$-aI$	$\frac{-sa\sqrt{2}}{2c}I$	$\frac{m_0}{2}\angle -60°$
5-6	$\frac{N}{6p}\left(6\frac{\alpha}{\pi}-1\right)$	$-k''I$	$-ak''I$	$a^{1/2}I$	$a^{1/2}$	$\frac{sa^{3/2}\sqrt{2}}{2c}I$	$k''I$	$-ak'I$	$a^{1/2}k''I$	$-aI$	$\frac{-sa\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0\angle -90°$
6-7	$\frac{N}{3p}\left(1-3\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$a^{1/2}I$	$a^{1/2}$	$\frac{sa^{3/2}\sqrt{2}}{2c}I$	$k''I$	$ak''I$	$a^{1/2}k''I$	0	0	$\frac{m_0}{2}\angle -120°$
7-8	$\frac{N}{6p}\left(6\frac{\alpha}{\pi}-1\right)$	$-k''I$	$-ak''I$	$a^{1/2}k'I$	$a^{1/2}$	$\frac{sa^{3/2}\sqrt{2}}{2c}I$	$-k'I$	$ak''I$	$a^{1/2}k''I$	$-I$	$\frac{-s\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0\angle -150°$
8-9	$\frac{N}{3p}\left(1-3\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''$	$-a^{1/2}k''I$	0	0	$-k'I$	$ak''I$	$a^{1/2}k''I$	$-I$	$\frac{-s\sqrt{2}}{2c}I$	$\frac{m_0}{2}\angle -180°$
9-10	$\frac{N}{6p}\left(6\frac{\alpha}{\pi}-1\right)$	$-k''I$	$ak'I$	$-a^{1/2}k''I$	aI	$\frac{sa\sqrt{2}}{2c}I$	$-k'I$	$ak''I$	$a^{1/2}k''I$	$-I$	$\frac{-s\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0\angle +150°$
10-11	$\frac{N}{3p}\left(1-3\frac{\alpha}{\pi}\right)$	$-k''I$	$ak'I$	$-a^{1/2}k''I$	aI	$\frac{sa\sqrt{2}}{2c}I$	$k''I$	$ak''I$	$a^{1/2}k''I$	0	0	$\frac{m_0}{2}\angle +120°$
11-12	$\frac{N}{6p}\left(6\frac{\alpha}{\pi}-1\right)$	$-k''I$	$ak'I$	$-a^{1/2}k''I$	aI	$\frac{sa\sqrt{2}}{2c}I$	$k''I$	$ak''I$	$-a^{1/2}k'I$	$-a^{1/2}$	$\frac{-sa^{3/2}\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0\angle +90°$
12-1	$\frac{N}{3p}\left(1-3\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$-a^{1/2}k''I$	0	0	$k''I$	$ak''I$	$-a^{1/2}k'I$	$-a^{1/2}$	$\frac{-sa^{3/2}\sqrt{2}}{2c}I$	$\frac{m_0}{2}\angle +60°$

we assume that we have enough slots in the armature to neglect the slot steps in the flux picture, that is, we consider the case of $N \rightarrow$ infinite, with the current belt width held constant. Using the results from the charts in Figures 2–4, the diagrams are shown in Figures 6–8 as well as the flux shapes for several positions of the time reference line. The construction is indicated by the line marked v. The corners of the polygons correspond to the armature points as marked in Figures 2–4.

It is interesting to note that for $\alpha = 0$–30 degrees, several slots do not carry any current. Therefore, in the corresponding Goerges polygon, these slots merge into one polygon corner as shown in Figure 6. They cause horizontal lines in the flux picture.

Leakage Reactance Versus Varying Armature Speed

It will be necessary to analyze the components of the armature leakage reactance before deriving the necessary formulas.

The armature is fed with a current of brush frequency f_B. The current in each conductor varies with the same frequency, that is, with the exception of the commutation cut, the time differential quotient of the conductor current will be proportional to that one of the brush current.

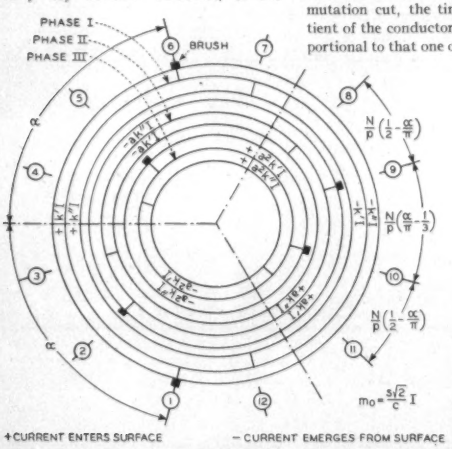

Figure 4. Current distribution, layer and slot magnetomotive forces in a 3-phase armature for $\alpha = 60$–90 degrees

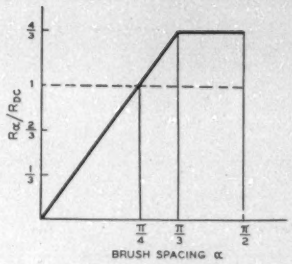

Figure 5. Effective phase resistance versus brush shifting angle

Each armature phase winding is now subjected to three different types of fluxes:

(*a*). The main (exciting) flux, crossing the air gap and interlinked with other windings in the machine.

(*b*). The leakage flux ϕ_0, caused by and interlinked only with this particular winding.

(*c*). The leakage flux ϕ_{00}, caused by and interlinked with all phase windings of the armature, but not interlinked with any other winding of the machine.

Flux (*a*) is disregarded as being immaterial to this particular consideration.

The fluxes ϕ_0 and ϕ_{00} are (neglecting leakage path saturation) proportional to the armature current and will be replaced later on by corresponding reactances X_0 and X_{00}.

Due to the commutator, the armature

Zone	No. of Slots	Upper Layer Current of Phase 1	Upper Layer Current of Phase 2	Upper Layer Current of Phase 3	Total Current in Upper Layer	Total Mmf in Upper Layer	Lower Layer Current of Phase 1	Lower Layer Current of Phase 2	Lower Layer Current of Phase 3	Total Current in Upper Layer	Total Mmf in Lower Layer	Total Mmf per Slot
1–2	$\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)$	$k'I$	$ak'I$	$-a^{1/2}k'I$	$-a^2I$	$\frac{-a^{3/2}\sqrt{2}}{2c}I$	$k''I$	$ak''I$	$-a^{1/2}k'I$	$-a^2I$	$\frac{-a^{3/2}\sqrt{2}}{2c}I$	$m_0/+60°$
2–3	$\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)$	$k'I$	$-ak''I$	$-a^{1/2}k''I$	I	$\frac{s\sqrt{2}}{2c}I$	$k''I$	$-ak'I$	$-a^{1/2}k'$	$-a^2I$	$\frac{-a^{3/2}\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0/+30°$
3–4	$\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)$	$k'I$	$-ak''I$	$-a^{1/2}k''I$	I	$\frac{s\sqrt{2}}{2c}I$	$k''I$	$-ak'I$	$-a^{1/2}k'I$	I	$\frac{s\sqrt{2}}{2c}I$	$m_0/0°$
4–5	$\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)$	$k'I$	$-ak''I$	$-a^{1/2}k''I$	I	$\frac{s\sqrt{2}}{2c}I$	$k''I$	$-ak'I$	$a^{1/2}k''I$	$-aI$	$\frac{-as\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0/-30°$
5–6	$\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)$	$k'I$	$-ak''I$	$a^{1/2}k'I$	$-aI$	$\frac{-as\sqrt{2}}{2c}I$	$k''I$	$-ak'I$	$a^{1/2}k''I$	$-aI$	$\frac{-as\sqrt{2}}{2c}I$	$m_0/-60°$
6–7	$\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$a^{1/2}k'I$	a^2I	$\frac{a^{3/2}\sqrt{2}}{2c}I$	$k''I$	$-ak'I$	$a^{1/2}k''I$	$-aI$	$\frac{-as\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0/-90°$
7–8	$\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)$	$-k''I$	$-ak''I$	$a^{1/2}k'I$	a^2I	$\frac{a^{3/2}\sqrt{2}}{2c}I$	$-k'I$	$-ak'I$	$a^{1/2}k''I$	a^2I	$\frac{as^2\sqrt{2}}{2c}I$	$m_0/-120°$
8–9	$\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)$	$-k''I$	$-ak''I$	$a^{1/2}k'I$	a^2I	$\frac{a^{3/2}\sqrt{2}}{2c}I$	$-k'I$	$ak''I$	$a^{1/2}k''I$	$-I$	$\frac{-s\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0/-150°$
9–10	$\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)$	$-k''I$	$ak'I$	$a^{1/2}k'I$	$-I$	$\frac{-s\sqrt{2}}{2c}I$	$-k'I$	$ak''I$	$a^{1/2}k''I$	$-I$	$\frac{-s\sqrt{2}}{2c}I$	$m_0/+180°$
10–11	$\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)$	$-k''I$	$ak'I$	$-a^{1/2}k''I$	aI	$\frac{as\sqrt{2}}{2c}I$	$-k'I$	$ak''I$	$a^{1/2}k''I$	$-I$	$\frac{-s\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0/+150°$
11–12	$\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)$	$-k''I$	$ak'I$	$-a^{1/2}k''I$	aI	$\frac{as\sqrt{2}}{2c}I$	$-k'I$	$ak''I$	$-a^{1/2}k'I$	aI	$\frac{as\sqrt{2}}{2c}I$	$m_0/+120°$
12–1	$\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)$	$-k''I$	$ak'I$	$-a^{1/2}k''I$	aI	$\frac{as\sqrt{2}}{2c}I$	$k''I$	$ak''I$	$-a^{1/2}k'I$	$-a^2I$	$-\frac{a^{3/2}\sqrt{2}}{2c}I$	$\frac{\sqrt{3}}{2}m_0/+90°$

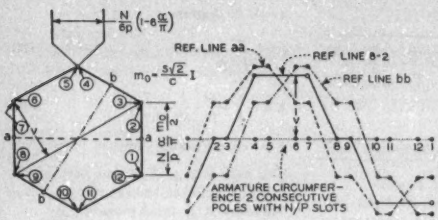

Figure 6. Goerges polygon and flux shapes for $\alpha = 0$–30 degrees

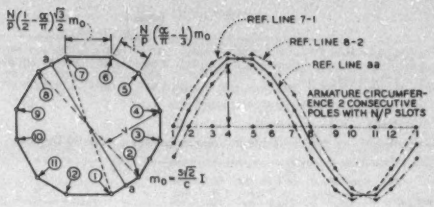

Figure 8. Goerges polygon and flux shape for $\alpha = 60$–90 degrees

can be considered as a normal 3-phase winding, stationary in space, with the winding axes being determined by the brush position. The flux ϕ_0 interlinked with one strand only, pulsates in space with brush frequency. The flux ϕ_{00}, which is actually the sum of three pulsating phase fluxes, rotates in space with brush frequency.

We imagine our leakage voltage to be generated by the armature conductors, rotating in space with speed frequency f_B, cutting the lines of the fluxes ϕ_0 and ϕ_{00}. The pulsating flux ϕ_0 is replaced by two rotating fluxes $\phi_0/2$, rotating in space with $+f_B$ and $-f_B$.

The amplitude of the induced leakage voltage is

$$E_L = K\left(\frac{\phi_0}{2}(f_B - f_R) + \frac{\phi_0}{2}(f_B + f_R) + \phi_{00}(f_B - f_R)\right) \quad (8a)$$

$$= K(\phi_0 f_B + \phi_{00}(f_B - f_R)) \quad (8b)$$

and substituting corresponding leakage reactances X_0 and X_{00}, both calculated at line frequency, we obtain

$$E_L = I\left(X_0\frac{f_B}{f_L} + X_{00}\left(\frac{f_B - f_R}{f_L}\right)\right) = IX_L \text{ per phase} \quad (8c)$$

The frequency of this leakage voltage, as appearing at the brushes, is always brush frequency.

Two practical cases are being considered.

(*a*). Brush frequency = line frequency (series and shunt motor)

$$f_B = f_L$$
$$f_R = (1 - S)f_L$$
$$X_L = X_0 + SX_{00} \quad (9)$$

(*b*). Brush frequency = slip frequency (Schrage motor)

$$f_B = Sf_L$$
$$f_R = -(1 - S)f_L$$

since the armature rotates opposite to the phase rotation,

$$X_L = SX_0 + X_{00} \quad (10)$$

Our conclusion is

(*a*). The total armature leakage reactance consists of two terms, one which remains constant and the other one being proportional to armature slip.

(*b*). The total armature leakage flux consists of two terms, one caused by and interlinked with one phase only, the other caused by and interlinked with all armature phases.

(*c*). The single interlinked leakage flux consists of a small part of the slot flux and a small part of the winding extension leakage flux.

(*d*). The double interlinked flux consists of most of the slot leakage flux, all of the differential or zig-zag or belt flux, all of the banding flux and most of the winding extension flux.

(*e*). The double interlinked leakage flux is usually much larger than the single interlinked leakage flux.

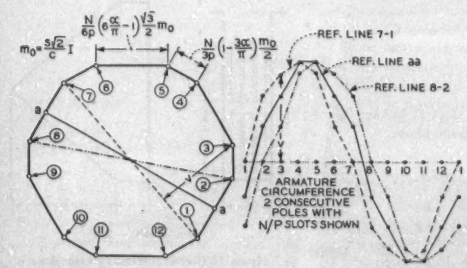

Figure 7. Goerges polygon and flux shapes for $\alpha = 30$–60 degrees

Differential Leakage Reactance

The derivation of the differential leakage will be the same as the method published by Liwschitz and his predecessors, except that the principal formula will be derived by means of the magnetic energy stored in the airgap.

Assuming a winding with M phases with the current I and infinite permeability in the iron, the magnetic energy stored in the airgap is

$$E_G = \frac{M}{4\pi f}X_G I^2 \quad (11)$$

The energy accounted for by the first harmonic is

$$E_1 = \frac{M}{4\pi f}X_m I^2 \quad (12)$$

with X_m the usual magnetizing reactance.

Inasmuch as the difference $E_G - E_1$ has to be accounted for in the total reactive energy balance, a virtual reactance, the differential reactance, is being introduced to

$$X_d = X_G - X_m \quad (13)$$

We can calculate the total magnetic energy in the air gap to

$$E_G = \frac{1}{2}\int^{G} h \cdot b \cdot dq \quad (14)$$

the integral being taken over the whole air gap and with

$dq = l_e \delta h_e dx$ volume element (14a)

$b = \Pi_0 h$ in air (14b)

$$\Pi_0 = \frac{4\Pi}{10}10^{-8} \quad (14c)$$

Introducing v = magnetomotive force in ampere turns at point x in the air gap to

$$v = \delta k_c h \quad (15)$$

Table I

α (Electrical Deg)	0	..	5	..	10	..	15	..	20	..	25	..	30	..	35	..	40	..	45
σ Per Cent	9.66	..	6.88	..	4.63	..	2.85	..	1.54	..	0.657	..	0.215	..	0.128	..	0.163	..	0.215
α (Electrical Deg)	50	..	55	..	60	..	65	..	70	..	75	..	80	..	85	..	90		
σ Per Cent	0.253	..	0.258	..	0.215	..	0.118	..	0.0408	..	0.0248	..	0.0773	..	0.165	..	0.215		

Table II. Summation for $\alpha = 60$–90 Degrees

Zone	No. of Commutator Bars	i_1, Current in Upper Conductor	i_2, Current in Lower Conductor	Total Reactance Voltage Per Zone
1–2	$u\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)$	$-a\frac{I}{c}$	$-a\frac{I}{c}$	$C\frac{p}{c}u\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)\left\{f_a(\lambda_{11}+\lambda_{21})\left(-a\frac{I}{c}\right)+2f_b\lambda_{12}\left(-a\frac{I}{c}\right)\right\}$
2–3	$u\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)$	$\frac{I}{c}$	$-a\frac{I}{c}$	$C\frac{p}{c}u\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)\left\{f_a(\lambda_{11}+\lambda_{21})\left(\frac{I}{c}\right)+2f_b\lambda_{12}\left(-a\frac{I}{c}\right)\right\}$
3–4	$u\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)$	$\frac{I}{c}$	$\frac{I}{c}$	$C\frac{p}{c}u\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)\left\{f_a(\lambda_{11}+\lambda_{21})\left(\frac{I}{c}\right)+2f_b\lambda_{12}\left(\frac{I}{c}\right)\right\}$
4–5	$u\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)$	$\frac{I}{c}$	$-a\frac{I}{c}$	$C\frac{p}{a}u\frac{N}{p}\left(\frac{1}{2}-\frac{\alpha}{\pi}\right)\left\{f_a(\lambda_{11}+\lambda_{21})\left(\frac{I}{c}\right)+2f_b\lambda_{12}\left(-a\frac{I}{c}\right)\right\}$
5–6	$u\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)$	$-a\frac{I}{c}$	$-a\frac{I}{c}$	$C\frac{p}{a}u\frac{N}{p}\left(\frac{\alpha}{\pi}-\frac{1}{3}\right)\left\{f_a(\lambda_{11}+\lambda_{21})\left(-a\frac{I}{c}\right)+2f_b\lambda_{12}\left(-a\frac{I}{c}\right)\right\}$

Total reactance voltage between brushes of the same phase

$$e=\frac{\pi^2}{5}\frac{2^2}{Nc^2}I\left\{f_a(\lambda_{11}+\lambda_{21})\frac{1}{3}+2f_b\lambda_{12}\times\left(\frac{\alpha}{\pi}-\frac{1}{6}\right)\right\}10^{-8}\cdot I \text{ volts}$$

we see that v may be taken directly from the Goerges diagram as the vertical projection on the time reference line.

After some straightforward calculations, we obtain

$$E_G=\frac{pl_1\Pi_0\tau}{\delta k_c N}\sum v^2 \tag{16}$$

$$X_G=\frac{4\pi f p l_1\Pi_0\tau}{\delta k_c N M_1^2}\sum v^2 \tag{17}$$

The summation $\sum v^2$ requires the sum of the square of all projections in the Goerges diagram over all teeth of the armature.

Since

$$X_m=\frac{MW^2_{eff}l_1\Pi_0 A f\tau}{\delta k_c p\pi} \tag{18}$$

$$X_d=\sigma X_m \tag{19}$$

with

$$\sigma=\left(\frac{p\pi}{MW_{eff}I}\right)\sum\left(\frac{v^2}{N}\right)-1 \tag{20}$$

Theoretically we should develop now a formula including all values of N and α. This leads to an extremely complicated procedure and we, therefore, resort to the well known approximation to split the differential leakage into

(*a*). Tooth-top leakage (added to slot leakage).

(*b*). Belt leakage (differential leakage for $N \to$ infinite).

The term $\sum v^2$ is nothing else but the axial moment of inertia of the Goerges polygon. For our assumption of $N \to$ infinite, it changes to

$$\sum v^2=\sum\int_{N1}^{N2} v^2(N)dN \tag{21}$$

the integral being taken over the boundary lines of the polygon. Since the polygon is symmetrical with regard to the center (no second harmonics), the axial moment, according to Richter,[4] is independent of the instantaneous position of the reference line.

The calculation of $\sum v^2$ is tedious, though straightforward and will not be reproduced here. Reference is made again to Richter[4] and Liwschitz.[3]

We finally obtain

$\alpha = 0$–30 degrees

$$\sigma=\frac{\pi^4}{9}\frac{\left(\frac{\alpha}{\pi}\right)^2}{\sin^2\alpha}\left(1-\frac{\alpha}{\pi}\right)-1 \tag{22}$$

which degenerates for $\alpha \to 0$ to

$$\frac{\pi^3}{9}-1 \tag{22a}$$

the same value as the belt leakage factor for one slot per pole and phase.

$\alpha = 30$–60 degrees

$$\sigma=\frac{8\pi^4}{9\sin^2\alpha}K-1 \tag{23}$$

$$K=-\frac{k^3}{4}+\frac{3}{16}k^2-\frac{k}{96}+\frac{1}{1{,}728} \text{ with } k=\frac{\alpha}{\pi} \tag{23a}$$

$\alpha = 60$–90 degrees

$$\sigma=\frac{8\pi^4}{9\sin^2\alpha}K-1 \tag{24}$$

$$K=-\frac{k^3}{8}+\frac{k^2}{16}+\frac{k}{32}-\frac{7}{1{,}728} \tag{24a}$$

The values for σ are shown in Table I.

This belt leakage X_d forms part of the double interlinked leakage reactance $X_{\sigma\sigma}$. Therefore, if the brush frequency is

$f_B = f_{line}$, X_d is proportional to slip S

$f_B = f_{slip}$, X_d is proportional to line frequency

Inasmuch as the winding in the opposite part of the machine is not a squirrel cage winding, any damping effects may be safely neglected.

Slot and Tooth-Top Leakage Reactance

We will proceed in the following manner: first, we calculate the leakage voltage per coil side, then per coil, then per commutator bar, and finally we sum up all bar voltages between brushes of the same phase. Our calculation will be exact only for integer values of $\frac{N}{P}\frac{\alpha}{\pi}$ but interpolations are permissible.

A slot of the shape shown in Figure 9 is assumed. For semienclosed slots, the respective values of λ will have to be changed in accordance with well known formulas.

Reactance voltage in upper coil side of coil considered

$$e_u=2\pi(f_a L_{11}i_1+f_b M_{12}i_2') \tag{25a}$$

with i_1 = current in upper coil side, i_2' = current in coil side below this particular coil side.

Reactance voltage in lower coil side of coil considered

$$e_l=2\pi(f_a L_{22}i_1+f_b M_{12}i_2'') \tag{25b}$$

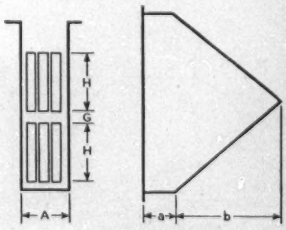

Figure 9 (left). Slot

Figure 10 (right). Winding extension

with $i_2'' =$ current in coil side above this particular coil side.

For 100 per cent coil pitch, $i_2' = i_2''$. As to f_a, f_b, we will designate their values later on.

$$L_{11,22} = \frac{4\pi}{10} l \lambda_{11,22} u r^2 10^{-8} \tag{26}$$

$$M_{12} = \frac{4\pi}{10} l \lambda_{12} u r^2 10^{-8} \tag{27}$$

$$\lambda_{11} = \frac{H}{3A} + \lambda_0 \tag{28a}$$

$$\lambda_{22} = \frac{H}{3A} + \frac{G+H}{A} + \lambda_0 \tag{28d}$$

$$\lambda_{12} = \frac{H}{2A} + \lambda_0 \tag{28c}$$

$\lambda_0 =$ slot permeance on top of upper coil, including tooth-top leakage (28b)

Reactance voltage per coil

$$e_{coil} = e_u + e_l = C\{f_a(\lambda_{11} + \lambda_{22})i_1 + 2f_b\lambda_{12}i_2\} 10^{-8} \text{ volt} \tag{29}$$

$$C = \frac{8\pi^2}{10} l u r^2 10^{-8} \tag{29a}$$

The voltage per commutator bar is

$$e_{bar} = e_{coil}\frac{p}{c} \tag{30}$$

and it remains to sum up these voltages between brushes of the same phase.

Such a summation is shown as a sample for $\alpha = 60$–90 degrees in Table II. The zonal points referred to correspond to the arrangement shown in Figure 4. Keeping in mind that

$$\frac{z}{4c} W = \frac{Nur}{2c} = \frac{sN}{4c} \tag{31}$$

we obtain as the result of the sample calculation and similar calculations

$\alpha = 0$–30 degrees

$$X_{slot} = \frac{\pi^2}{5}\frac{z^2}{Nc^2} l \left\{ f_a(\lambda_{11} + \lambda_{22})\frac{\alpha}{\pi} \right\} 10^{-8} \tag{32a}$$

$\alpha = 30$–60 degrees

$$X_{slot} = \frac{\pi^2}{5}\frac{z^2}{Nc^2} l \left\{ f_a(\lambda_{11} + \lambda_{22})\frac{\alpha}{\pi} + 2\lambda_{12} f_b\left(\frac{\alpha}{\pi} - \frac{1}{6}\right) \right\} 10^{-8} \tag{32b}$$

$\alpha = 60$–90 degrees

$$X_{slot} = \frac{\pi^2}{5}\frac{z^2}{Nc^2} l \left\{ f_a(\lambda_{11} + \lambda_{22})\frac{1}{3} + 2\lambda_{12} f_b\left(\frac{\alpha}{\pi} - \frac{1}{6}\right) \right\} 10^{-8} \tag{32c}$$

with l measured in centimeters.

The first term within the bracket corresponds to the leakage flux interlinked with one phase only; the second to the double interlinked flux. Consequently we can substitute for

$$f_B = f_L \quad f_a = f_L \quad f_b = Sf_L$$
$$f_B = Sf_L \quad f_a = Sf_L \quad f_b = f_L$$

End Extension Reactance

One ampere, flowing in an end extension conductor, will excite

$$\frac{4\pi}{10}(\lambda' + \lambda'')$$

magnetic flux lines per one inch length. Part of these flux lines will be interlinked with the conductor only (λ') and the rest with all other phases (λ''). The values λ' and λ'' depend very much upon the mechanical shape of the extension, steel banding and distance between primary and secondary winding. Unfortunately, these constants are not as accessible to mathematical treatment as the other leakage constants.

If we assume now that the magnetomotive force of the current belt along the extension is a sine wave (neglecting higher harmonics and thus committing an error of the magnitude of $\sqrt{\sigma}$), we may write

$$X_{ext} = 4\pi \frac{M}{2} p(f_a L' + f_b L'') \tag{33}$$

with L being the inductance correponding to λ' and λ''.

$$L = \frac{W^2_{eff}}{p^2} l_{ext} \lambda \frac{4\pi}{10} 10^{-8} \tag{33a}$$

$$X_{ext} = \frac{8\pi}{10}\frac{W^2_{eff}}{p}(f_a\lambda' + f_b\lambda'') l_{ext} M 10^{-8} \tag{34}$$

Very hesitatingly, since each designer has to derive this formula for his type of design, the author quotes

$$l_{ext}\lambda'' = 0.82\left(a + \frac{b}{2}\right) \tag{35}$$

with reference to Figure 10 and a and b measured in inches.

Corrections for banding and proximity of iron are not included in this formula.

Since the conductors of the extension are close together in most designs, λ' can be neglected and the end extension leakage flux is interlinked entirely with all phases. Hence, we can write for

$$f_B = f_{line};\ f_b = Sf_L;\ f_a = 0 \tag{36a}$$

$$f_B = Sf_{line};\ f_b = f_L;\ f_a = 0 \tag{36b}$$

It is, however, not too difficult to measure the leakage end extension flux with a probing coil and thus actual values of λ'' may be obtained.

Conclusion

In a 3-phase armature with two brushes per phase and coils with 100 per cent pitch, the current distribution in the conductors is independent of the resistance and leakage values of the individual branches, regardless of brush shift. Hence, simple effective resistance and leakage reactance formulas may be derived. The total leakage flux is split up into a single and double interlinked flux, creating two components of the leakage reactance, which vary in a different manner with regard to the speed of the armature. The derivation presented covers all resistance and leakage reactance values as a function of the brush spacing as well as the Goerges diagram and the shape of the armature flux. This method of analysis is adaptable to all resistance and reactance calculations for windings with or without commutators.

List of Symbols

$\alpha =$ brush spacing angle in electrical degrees (total electrical angle between phase brushes $= 2\alpha$)
$a =$ operator $= e^{j120°}(1 + a + a^2 = 0)$
$b =$ magnetic field density
$c =$ number of pairs of parallel branches in armature ($= 1$ for single wave winding)
$\delta =$ mechanical air gap in centimeters
$DF_N =$ distribution factor for nth harmonic
$e_u, e_l =$ reactance voltage in upper, lower coil side
$E_G =$ magnetic energy in gap
$E_1 =$ magnetic energy of first harmonic flux
$f_B =$ brush frequency
$f_R =$ rotational frequency
$f_L =$ line frequency
$f_a f_b =$ frequencies as defined in text
$\phi_0, \phi_{00} =$ leakage fluxes
$h =$ magnetic field strength
$I =$ brush current
$I', I'' =$ branch currents
$i_1, i_2', i_2'' =$ currents in coil sides
$k', k'' =$ complex factors (absolute value smaller than unity)
$k_c =$ Carter factor
$k = \frac{\alpha}{\pi}$
$K =$ constant
$l =$ actual core length in centimeters
$l_l =$ effective core length in centimeters
$l_{ext} =$ average extension length per conductor per side
$L_{11}, L_{11}, L_{22}, L_{12} =$ inductances
$\lambda_{11}, \lambda_{22}, \lambda_{12}, \lambda_0 =$ slot leakage permeance
$\lambda', \lambda'' =$ coil extension leakage permeance
$m_0 =$ maximum magnetomotive force per slot in ampere turns
$M =$ number of phases
$M_{12} =$ mutual inductance
$n =$ integer of harmonic
$N =$ number of slots
$p =$ number of pole pairs

r = turns per coil side

R_a = effective phase resistance

R_{dc} = diameter brush resistance (d-c resistance)

s = total conductors per slot

S = slip

σ = belt leakage factor

τ = pole pitch in centimeters

u = coil sides per slot layer

v = magnetomotive force in air gap in ampere turns

W = turns per phase

W_{effn} = effective turns per phase for nth harmonic

x = free co-ordinate along armature periphery

X_{1s}, X_{2s} = leakage reactance

X_G = total reactance of air gap

X_L = total leakage reactance

X_m = magnetizing reactance for 1st harmonic

X_d = differential reactance

z = total conductors in armature $= sN = 2urN$

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No Discussion

Quadrature Operation of Filamentary Thermionic Gas Tubes

V. L. HOLDAWAY
ASSOCIATE AIEE

Synopsis: Filamentary thermionic gas tubes, when operating with the anode and filament voltage inphase, exhibit a marked temperature gradient along the filament. When the magnitude of the anode and filament currents are of comparable value, the resulting nonuniformity in filament temperature may be sufficient to cause short tube life and unreliable operation. This condition is not corrected appreciably by any special arrangement of anode current return connection to the filament circuit.

By operating the tube with the anode and filament voltages in quadrature,[1] approximately double the value of current permissible under the inphase operating condition may be conducted before the same temperature difference results.

General

THE CATHODE in a thermionic gas tube is capable of supplying an emission current of several amperes per square centimeter. When the tube is of the oxide-coated filamentary type, the maximum rated peak anode current may be larger than the nominal filament current, and the rated average anode current may be a substantial fraction of the nominal filament current. This is true especially of the low-voltage rectifier tubes and thyratrons.

In the most common use of these tubes in rectifier or inverter circuits, the filament is supplied normally from an alternating voltage and the anode current is caused to flow for some fraction of the cycle of the fundamental frequency. Considering the single-phase circuit with the filament and anode voltage inphase, the anode current will be conducted during a portion of the cycle in which the filament current flows in one direction.

The current in the filament from point-to-point will not be equal due to the emission current leaving the filament and unequal heating will result. When the value of anode current returned to the filament is comparable in value to the filament current an appreciable filament temperature change is produced.

Two other effects which cause a filament temperature change but which are secondary to the ohmic loss are electron evaporation and positive ion bombardment.[2,3] The cooling effect resulting from electron evaporation and the heating effect resulting from positive ions returning to the filament tend to cancel. In the mercury vapor and low pressure gas-filled rectifiers and thyratons considered in this paper, the net temperature change from these two factors was found to be small. This is not true of all types of thermionic gas tubes, however, as for example in the gas-filled tungsten-filament tungar type rectifier. In this type of tube the high emission current density, high-gas pressure and low tube voltage drop produces an appreciable net electron cooling effect. A filament temperature drop of over 100 degrees centigrade may be observed at full load.

In the design of the emitter for a thermionic gas tube, the optimum filament temperature has been determined by life tests for the various tube types and uses. For any particular type and use, plus or minus 25 degrees centigrade would be a representative allowable variation about the design center.

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V. L. HOLDAWAY is a member of the technical staff of Bell Telephone Laboratories Incorporated, New York, N. Y.

To allow this total variation in temperature to appear along the filament in an individual tube, however, would be very undesirable. Nonuniform temperatures and the resulting nonuniform emission has several deleterious effects:

1. Active material is lost more rapidly from the hot area as a result of the increased rate of evaporation.

2. Hot-spots are likely to form which may result in destructive forward arcing.[4]

3. The rapid rate of accumulation of active materials on the anode increases the likelihood of arc-back.[5]

4. The cold area, as well as not supplying the proper share of emission current, is more likely to accept deposits of sputtered material. The resulting overlay of contaminants greatly reduces the emission capabilities of that area. All of these contribute to unreliable operation and shorter tube life.

Determination of Temperature Difference

To evaluate the filament temperature change resulting from the conduction of anode current, tests were made under four circuits conditions as shown in Figures 1*a*, 2*a*, 3*a*, and 4*a*. A half-sine wave of anode current was conducted in the tests as the wave shapes in actual rectifiers and inverters differ so greatly it is not possible to select any one shape as typical. The filament temperature was measured by means of an optical pyrometer having a sensitivity of plus or minus five degrees centigrade. The filament was viewed during the interval when the anode current was zero by interposing a synchronous motor-driven rotary shutter between the observer and the pyrometer.

Tests were made on a laboratory tube model having a 2.5-volt 3.0-ampere folded mesh filament, and on a special Western Electric *267B* tube provided with observation holes in the filament heat casement. The latter tube is a medium power half-wave mercury vapor rectifier. It employs a shielded oxide-coated folded nickel mesh filament. The filament is designed to operate at 5.0 volts and the nominal filament current is 6.75 amperes. The peak inverse

anode voltage rating is 7.5 kv. The anode current rating for inphase operation is 1.0-ampere average and 4.0-amperes peak, and for quadrature operation 2.0-amperes average and 8.0-amperes peak.

The results of the filament temperature tests on the laboratory model and the *267B* type were similar and only those on the latter type are presented.

Inphase Operation

In the single-phase circuit the tube conducts for approximately one-half cycle. When the filament and anode voltages are inphase, the filament current flows in one direction only during the anode conduction period. Under these conditions it will be noted, by referring to Figure 1*a*, that the anode current passing through the filament circuit adds to the filament current at one end of the filament and substracts from the filament current at the other. The I^2R power input is hence unequal near the two filament ends, causing increased heating of one and decreased heating of the other. The resulting temperature difference causes the hotter end to supply a greater share of the emission current which further accentuates the temperature difference.

The d-c resistance of the low-voltage winding of the filament transformer is, in general, less than 10 per cent of the filament resistance. This results in a reasonably equal division of the anode current returned to the filament ends, regardless of the point at which the anode circuit returns to the filament transformer. There is sufficient inequality of current division, however, to produce a noticeable change in the temperature of the filament midpoint and a smaller change in the temperature of the filament ends, as shown by the data.

The data are presented in graph form. Filament brightness temperatures in degrees centigrade are plotted as ordinate against the ratio of anode current to filament current, expressed as peak values, as abscissce. An insert *a* of the circuit, and *b* of the representative current waves, is included in each figure.

Figure 1 shows the resulting filament temperatures when the anode current return is connected to one end of the filament. The transformer polarity is such that the filament and anode currents are aiding in the number 1 end of the filament. At some point along the filament the anode current will be zero. This point will lie between the midpoint and the number one end because of the transformer resistance. Between this point and the number one end the two currents are aiding and cause increased heating, while between this point and the number two end the currents are in opposition and cause decreased heating. As the resistance per inch of filament is considerably higher in the hot end, and as a major share of the emission current is supplied by this end, the filament mid-

Figure 1. Inphase operation

Anode current return connection to filament end. Phase angle = 0 degrees. Western Electric 267*B* tube

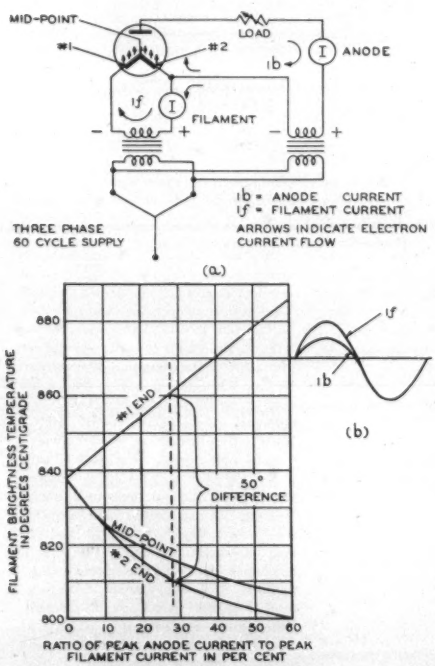
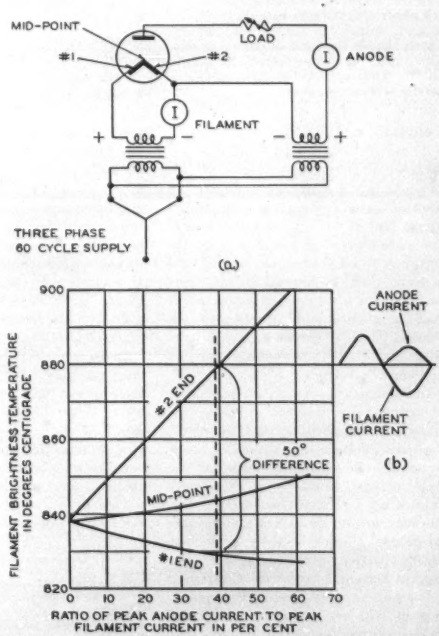


Figure 2. Inphase operation

Anode current return connection to filament end. Phase angle = 180 degrees. Western Electric 267*B* tube



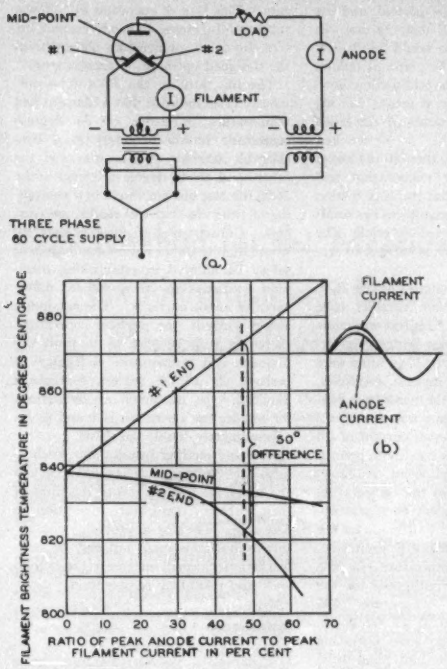


Figure 3. Inphase operation

Anode current return connection to filament transformer midpoint. Phase angle = 0 degrees. Western Electric 267B tube

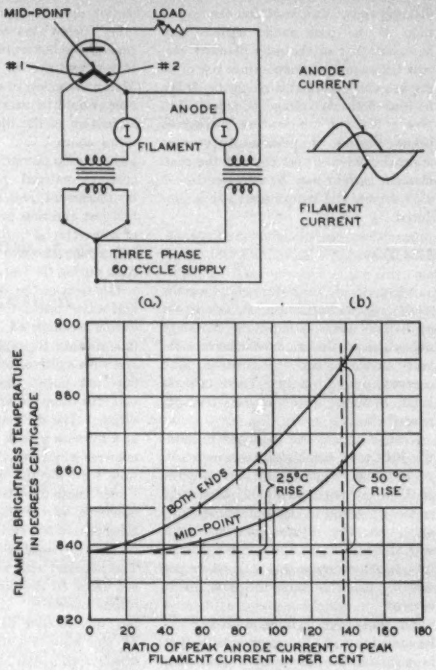


Figure 4. Quadrature operation

Anode current return connection to filament end. Phase angle = 90 degrees. Western Electric 267B tube

point and the number 2 end operate at greatly reduced temperatures. A temperature difference of 50 degrees centigrade is reached between the filament ends when the peak anode current is approximately 30 per cent of the peak filament current. This circuit is the least desirable as three-fourths of the filament operates below normal temperature.

The effect of reversing the polarity of the filament transformer is shown in Figure 2. In this case the point along the filament at which the anode current is zero will be nearer the cool end of the filament. The hot end operates at a temperature slightly higher than in the preceding case but the temperature gradient is more uniform and the midpoint operates at a temperature slightly above normal. The ratio of peak anode current to peak filament current is approximately 40 per cent when an end temperature difference of 50 degrees is reached.

Figure 3 gives the performance when the anode current return is made to the midpoint of the filament transformer. The temperature conditions are slightly improved over the preceding two conditions and a peak anode current of approximately 50 per cent of the peak filament current may be conducted before the arbitrary 50 degree centigrade difference is attained.

It may be pointed out that an anode circuit return made directly to the midpoint of the filament would further improve the conditions. The maximum heating effect would occur directly to one side of the return point and the maximum cooling effect directly to the other side. The two opposing effects would tend to cancel each other by radiation and heat conduction along the filament.

Quadrature Operation

Filament temperatures resulting under quadrature operating conditions are shown in Figure 4. The filament voltage is taken from line to line of the three-phase power supply and the anode voltage taken from line to neutral as indicated in Figure 4*a*. This provides a 90 electrical degree phase angle between the filament and anode voltages. The anode circuit return is made to one side of the filament as only a small gain in filament temperature uniformity is obtained by a transformer mid-point return.

When the tube is operated with the filament and anode voltages in quadrature, the filament current flows in one direction for one-half of the anode conduction period and in the opposite direction for the other half, Figure 4*b*. This results in a symmetrical heating and cooling effect in the two filament ends. The net result is a slight increase in the temperature of the filament midpoint and a larger but equal increase in temperature of the

filament ends. It is seen that the magnitude of the peak anode current may approach that of the peak filament current before an end temperature rise of 25 degrees above normal is reached. If the no-load filament voltage is adjusted to give a filament temperature 25 degrees below normal, a peak anode current approximately 140 per cent of the peak filament current may be tolerated before a 50 degrees end temperature rise is produced.

Life Tests

Adequate life tests demonstrating the merits of quadrature operation are not available. Tests attempting to show differences of this kind conclusively involve large numbers of tubes and are time consuming and costly. There are at hand, however, some life data showing related effects.

Tests conducted on the Western Electric *301A* tube, a small full-wave mercury-vapor rectifier, indicate the adverse effect of high current operation. This tube employs a 5-volt 3-ampere filament divided into two separate sections, each with its enclosing anode. It is rated at 0.5-ampere average anode current per section and 2.0-amperes peak anode current.

The tubes were operated under an overload condition of 1.75-amperes d-c output in a full-wave circuit with inphase filament excitation. This corresponds to a peak current of approximately 2.5 amperes and a ratio of 60 per cent peak anode current to peak-filament current. The temperature difference between the ends of the filament was approximately 85 degrees centigrade. The life obtained under these conditions was approximately one-third normal. While no comparative quadrature life runs were made on this tube type, the manner in which the filament failed is significant. The two filament sections are in series which results in one section operating above normal, and the other below normal temperature. In general the hot section would be the first to lose emission to the point of failure. The emission loss of the cold section, however, would be nearly as great. An examination of the filaments of the failed tubes showed that the hot section had lost emission primarily through the loss of coating material by evaporation and bombardment, and that the cold section had lost emission primarily as the result of an overlay of sputtered material. The hot section also was more subject to arcback during the test.

Life tests on the Western Electric *267B* half-wave mercury-vapor rectifier tube were not completed. This test was made in a six-tube three-phase bridge type circuit with a filtered load. Two tubes were operated under the inphase condition, and four tubes under the quadrature condition. The d-c output was 5 amperes at 9 kv with a peak anode current of 5.5 amperes which was the maximum permissible load for the equipment available. Under these conditions the temperature difference of the filament ends was approximately 50 degrees centigrade for the tubes with inphase filament excitation. The filament end temperature rise was less than 10 degrees centigrade for the tubes operating in quadrature. The test was discontinued at 11,000 hours. At this time all tubes were operating satisfactorily, but the individual tube tests indicated a somewhat greater loss of emission for the tubes in the inphase condition.

Conclusions

Inphase operation of filamentary gas-filled rectifier and thyratron tubes at high anode currents results in an undesirable temperature difference along the filament.

This condition may be improved considerably by operating the tube with the anode and filament currents in quadrature. This type of operation will allow a substantial increase in anode current before the same temperature change from the designed optimum is encountered.

The life tests on the *301A* tube previously cited indicate that a filament end temperature difference of 85 degrees centigrade results definitely in a considerable sacrifice of tube life. On the other hand, the 50 degree difference in the *267B* life test did not result in a shortening of tube life that was readily measurable. Conservative engineering practice would dictate that a reasonable margin of safety be allowed in establishing maximum permissible temperature differences or anode currents. The maximum anode current for inphase operation, therefore, may be such as to limit the filament end temperature difference to perhaps 25 to 50 degrees centigrade. Similarly, the maximum anode current for quadrature operation is found to be approximately double this value.

In many rectifier installations, such as large high-voltage rectifiers, even though the tubes are not worked to the limit of their ratings, quadrature operation is desirable. The cost of providing quadrature excitation is small and may be justified by even a small increase in life expectancy and reliability of operation.

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No Discussion

A Method for the Measurement of the Ionization and Deionization Times of Thyratron Tubes

MILTON BIRNBAUM
NONMEMBER AIEE

IN THE DESIGN of pulse circuits employing thyratron tubes, the ionization and deionization times are important tube characteristics which determine to a considerable extent the operation and limitations of the particular circuit in which they are employed. Nevertheless, there exists little published information on these tube characteristics which could serve as a basis for circuit design. Although several studies have been undertaken to establish the mechanism of thyratron breakdown and extinction, and scientific descriptions of the processes are presented, none has set forth a method of computing these quantities which produce results in close agreement with laboratory measurements.[1–4]

In view of this situation, the measurements described in this paper were undertaken at the Naval Research Laboratory to provide data on the ionization and deionization times of several common types of thyratron tubes. At the time the author was securing this information, the emphasis was upon speed rather than completeness and, therefore, not all available tube types were included in the tests, nor were the effects of the variations of all the circuit parameters upon ionization and deionization times fully investigated.

Ionization-Time Measurements

The ionization time of a thyratron is broadly speaking, a measure of the time interval between the application of a trigger pulse and the time at which the thyratron starts conduction. By using a rectangular trigger pulse, with a rise time that is small as compared to the ionization time of the thyratron, the ionization time as a function of the amplitude of the trigger pulse is obtained. If a trigger pulse with a sloping front is employed, the attempt to plot starting time as a function of trigger amplitude encounters the following limitation. As the amplitude of the trigger pulse is increased, a point will be reached where ignition of the thyratron occurs along the sloping front of the trigger pulse, and further increases in the trigger amplitude, unless the slope of the pulse changes, need not alter the starting time. To extend the graph to regions of shorter starting times, trigger pulses with faster rise times must be employed; to explore the regions of shortest starting times, rectangular trigger pulses are required.

A circuit capable of generating rectangular pulses with rise times less than 0.01 microsecond and with amplitudes greater than 200 volts is shown in Figure 1. Upon ignition of the *2D21* thyratron, the charged delay line in its anode circuit commences to discharge through the cathode load resistor and the *2D21*. This flow of current continues for a period determined by twice the length of the delay line, since this is the time required for the step voltage of negative polarity to travel down the line, suffer a reflection without a polarity change at the open end of the line, and return to the anode. The appearance of this negative pulse at the anode pulls the anode down to a potential below the cathode potential and conduction ceases. The necessary condition for circuit operation in the manner just described is that the cathode load resistor be less than, or at most equal to, the characteristic impedance of the delay line. The resistor in the thyratron anode circuit must be so selected that the *RC* time constant (the *C* here is the line capacitance) is sufficiently short to allow the line to recharge fully at the required repetition rate and that the rate at which the line recharges is slow enough to permit the tube to deionize before the anode rises to a voltage high enough to produce reignition. The use of a low-impedance line is consistent with the rapid pulse rise time requirement since a large value of peak current is necessary to charge the stray circuit capacitances in less than 0.01 microsecond.

The following expedients were resorted to in order to produce a flat-topped pulse: use of a delay line with distributed parameters (a roll of 75-ohm coaxial cable) and a 150-ohm resistor in the grid circuit of thyratron 2. With this arrangement, the peak to peak ripple voltage along the top of the pulse rarely exceeded ten per cent of the pulse amplitude.

By variation of the anode voltage of thyratron 1, the output pulse can be

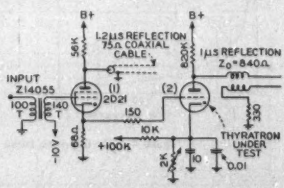

Figure 1. Circuit for ionization-time measurements

Input—A positive trigger at approximately 50-volt amplitude and of 1-microsecond duration supplied by a synchroscope

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MILTON BIRNBAUM is with the Naval Research Laboratory, Washington, D. C.

The methods described for measurement of both the ionization and deionization time characteristics of thyratrons were suggested by F. H. Wells, who supervised this work. The views presented in this paper represent those of the author and are not necessarily those of the Naval Research Laboratory.

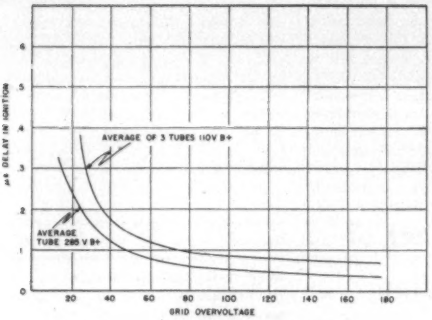

Figure 2. Starting-time curves (2A4)

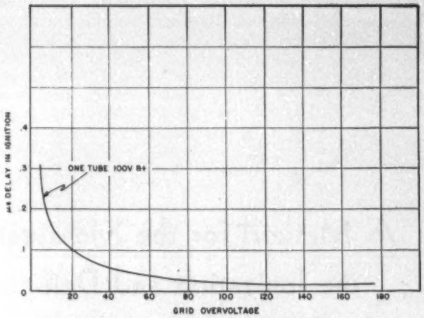

Figure 5. Starting-time curve (6D4)

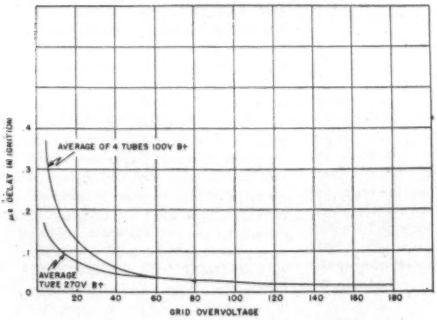

Figure 3. Starting-time curves (2C4)

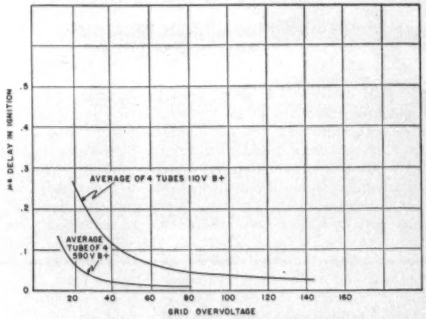

Figure 6. Starting-time curves (2050)

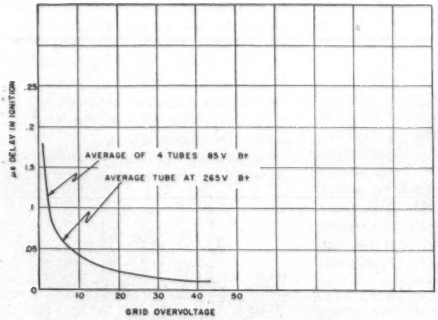

Figure 4. Starting-time curve (884)

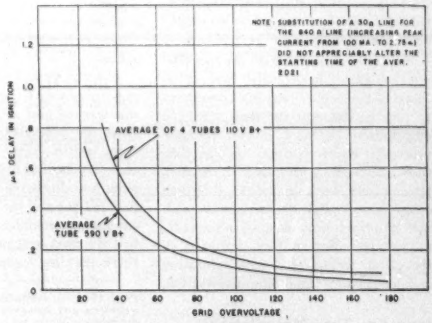

Figure 7. Starting-time curves (2D21)

varied in amplitude from 250 volts to 50 volts. Below 50 volts, the pulse rise time commences to deteriorate rapidly. Therefore, to produce a satisfactory pulse less than 50 volts in amplitude, a 150-ohm potentiometer is connected in parallel with the 68-ohm resistor in the cathode of thyratron 1. The output is then taken from the potentiometer tap and may be varied by changing the position of the tap. This procedure was not employed throughout the tests since the potentiometer in the cathode of thyratron 1 resulted in an increase in the ripple voltage on the pulse top.

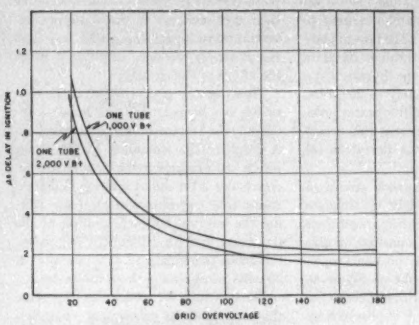

Figure 8. Starting-time curves (3C45)

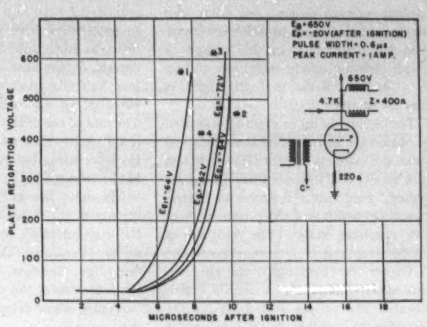

Figure 10. Deionization-time curves; tube type 884

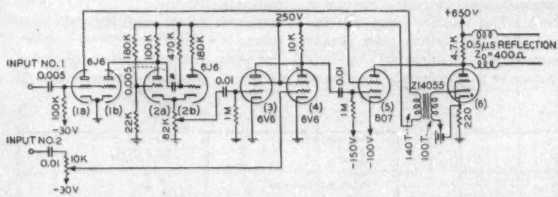

Figure 9. Circuit for deionization-time measurements

Input 1—Fixed positive pulse (approximately 10 microseconds wide, 50-volt amplitude)

Input 2—Delayed positive pulse (approximately 20 microseconds wide, 50-volt amplitude)

The starting-time measurements were obtained by the use of a synchroscope which supplied triggers for the pulse-generator circuit and a linear sweep with a speed of one centimeter per 0.1 microsecond. By first displaying on this sweep the leading edge of the rectangular pulse and then the leading edge of the thyratron 2 pulse, and noting the distance between the two edges, the time interval between them could be estimated with an accuracy of ±0.01 microsecond. The distance between the leading edges of the two pulses was taken as the distance between the center section of each pulse front.

An alternate method of measuring the thyratron ionization time provided an excellent verification of the results obtained by the procedure just outlined. If the inspection probe of the synchroscope is placed upon the grid of thyratron 2, the rectangular pulse is seen on the oscilloscope screen, and the instant thyratron 2 breaks into conduction is indicated by the deformation of the thyratron 1 pulse. The deformation of the thyratron 1 pulse at that instant is due to the change in grid impedance of thyratron 2 when ignition of thyratron 2 occurs. This procedure is equivalent to that of displaying the thyratron 1 and the thyratron 2 pulses simultaneously.

Table I of starting time characteristics for rectangular trigger pulses is provided as a reference guide and also as a summary of the data contained in Figures 2–8. Most of the tube types were tested at two values of anode voltage. In this respect, special attention should be given to tube type *2050*, since its variation of starting time with anode voltage (for the same grid overvoltage) is considerable.

Table I. Thyratron Starting-Time Characteristics

Tube Type	Anode Voltage	Ignition Time in μ sec for Grid Overvoltage of 50 Volts	Reference
2A4	110	0.14	Figure 2
2C4	100	0.04	Figure 3
884	85	<0.01	Figure 4
6D4	100	0.04	Figure 5
2050	110	0.08	Figure 6
2D21	110	0.41	Figure 7
3C45	1000	0.50	Figure 8

Deionization-Time Measurements

In order to measure the deionization-time characteristics of thyratron tubes, the following procedure was adopted. The thyratron is brought into conduction and immediately after this, the anode of the thyratron is maintained at a predetermined potential below its cathode potential. At various intervals of time, the anode voltage is suddenly applied. The maximum voltage which can be

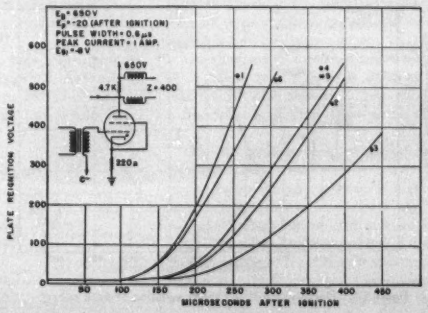

Figure 11. Deionization-time curves; tube type 2050

applied without causing reignition, is a measure of the extent to which the thyratron has deionized. In this manner, a graph showing anode reignition voltage as a function of the time after ignition may be plotted.

The circuit shown in Figure 9 was used to obtain the deionization time data. A pulse and sweep generator (Hazeltine test rack model *1021*) supplies the fixed pulse (called "fixed" since it serves as a reference time) which is applied to the grids of *6J6* amplifier stage. The outputs of triodes (1*a*) and (1*b*) serve respectively, to trigger the thyratron 6 and the one-kick multivibrator (2). From the multivibrator, the proper grid drive for the amplifier (3) is obtained. It consists of a negative pulse longer in duration than twice the deionization time of the thyratron under test (a 1,000-microsecond pulse fulfilled this requirement for all the tubes tested) and is adjustable in amplitude by means of the potentiometer in the cathode of the multivibrator (2). This pulse is amplified by the *6V6* (3), and the output (a pulse of positive polarity) is used to drive the grid of tube 5. The flow of plate current of amplifier 5 through its plate load, holds the anode of the thyratron at a potential of approximately −20 volts. The rate of rise of the multivibrator pulse is sufficiently slow to prevent this action from occurring before the thyratron (6) has been triggered.

The delay line in the anode circuit of thyratron (6) is used only to simulate the contemplated operating conditions of the thyraton. The omission of this delay line, therefore, has no effect upon the operation of the circuit of Figure 9.

A pulse, whose delay with respect to the fixed pulse may be varied, is provided by the pulse generator. The delayed pulse is amplified by the *6V6* (4) and its output (a negative pulse) also drives the grid of tube 5. This negative pulse, superimposed upon the positive pulse already present on the grid of the *807* (5), serves to reduce or to stop entirely the flow of plate current in tube 5. The delay line then may recharge to the *B* supply potential or to whatever voltage less than the *B* supply voltage, amplifier 5 holds the anode of the thyratron.

Thus, for any given time after the thyratron has been ignited, its anode voltage may be increased until it is reignited. A graph of the maximum plate voltage which just produces reignition versus microseconds after initial ignition (a deionization-time curve) can be plotted. During the interval between ignition of the thyratron and its subsequent reignition, the anode is maintained at a potential of 20 volts below that of the cathode.

The time interval between ignition of the thyratron and its subsequent reignition was measured by simultaneously displaying upon a 5-inch oscilloscope screen, the fixed pulse, the delayed pulse, and a sweep marker wave form consisting of the oscillations of a shock-excited tuned circuit. The sweep generator supplied the oscilloscope with sweeps of 5 microseconds, 25

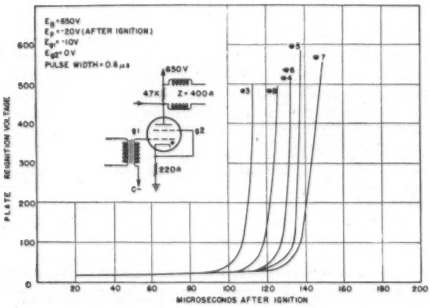

Figure 12. Deionization-time curves; tube type 2D21

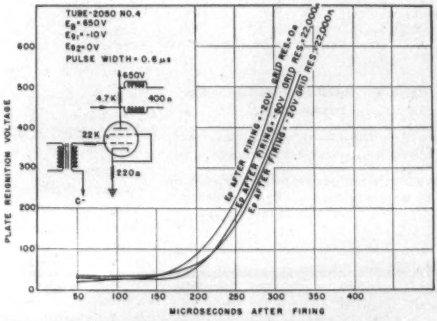

Figure 14. Deionization-time curves; effect of grid resistance and variation of plate voltage after ignition

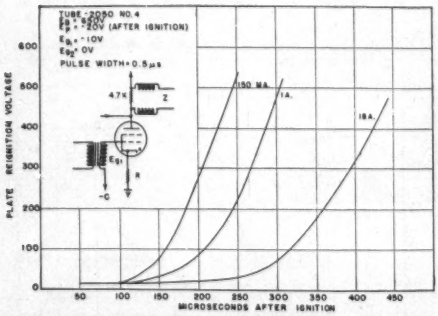

Figure 13. Deionization-time curves; variation of peak current

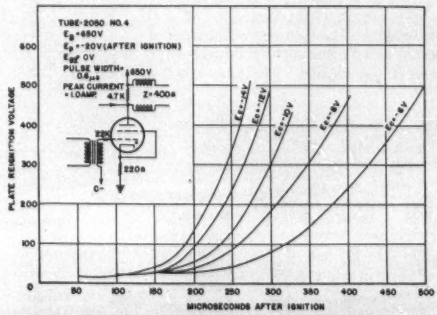

Figure 15. Deionization-time curves; variation of control grid bias

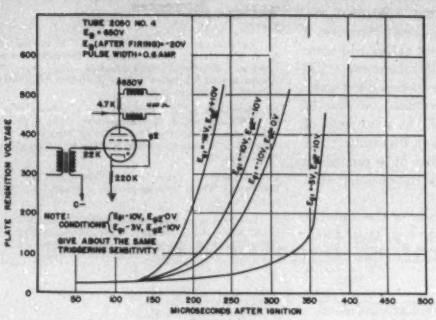

Figure 16. Deionization-time curves; variation of control grid and shield grid biases

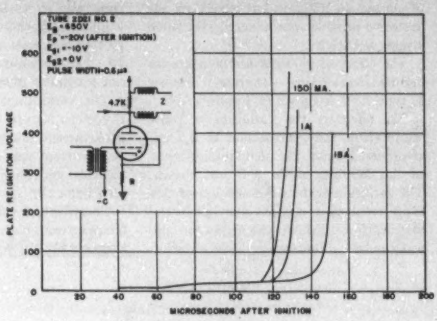

Figure 18. Deionization-time curves; variation of peak current

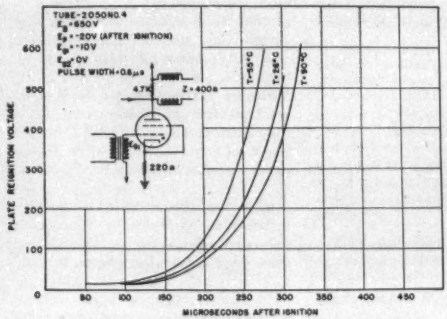

Figure 17. Deionization-time curves; ambient temperature varied

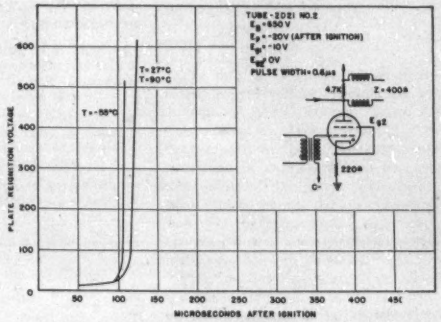

Figure 19. Deionization-time curves; ambient temperature varied

microseconds, 100 microseconds, 500 microseconds, and 2,000 microseconds with marker wave forms for each sweep. Each marker wave form consisted of 10 oscillations of a shock-excited tuned circuit, the time of one oscillation equaled 1/10 the duration of the sweep. The measurement could be obtained with a precision of 1/4 the time of an oscillation, or about 10 microseconds on the 500-microsecond sweep. Consequently, when observing the wave forms on a 500-microsecond sweep, no attempt was made to compensate for the error introduced by the sloping test pulse (wave form of the charging delay line) since this was small as compared to the possible error involved in estimating the time interval between the fixed and delayed pulses. Depending upon the voltage at which reignition occurs, the time delay in attaining this voltage may range from zero to several microseconds at 400 to 500 volts. When faster sweeps were employed (less than 100 microseconds in duration) and the precision of the measurement approached the magnitude of the error introduced by the sloping test pulse, another method of observation was employed wherein the sloping test pulse was displayed and its time of rise could be estimated.

Three tube types, the *884*, the *2050*, and *2D21* were subjected to deionization-time tests. The relevant characteristics of these tubes and the results of the tests are given in Table II.

The deionization-time characteristics of four thyratrons, type *884*, are shown on Figure 10. The biases indicated were obtained by biasing each tube 6 volts in excess of that required to prevent breakdown. The deionization-time characteristics of six thyratrons, type *2D21*, are shown on Figure 12. The numbers refer to the tubes and not to the curves.

Table II

Tube Type No.	Number of Electrodes	Gas	Gas Pressure, Microns	Reference
884	Triode	Argon	250	Figure 10
2050	Tetrode	Xenon	180	Figure 11
2D21	Tetrode	Xenon	150	Figure 12

The effect of variation of peak current upon the deionization-time of thyratron number 4, type *2050*, (shown on Figure 13) was determined by using delay lines of different characteristic impedances and corresponding cathode resistors.

The variation of the deionization time of thyratron number 4, type *2050*, as a function of grid resistance and anode voltage after ignition is shown on Figure 14.

The deionization-time variation of thyratron number 4, type *2050*, as a function of control grid bias is shown on Figure 15.

The effect of control grid and shield grid biases on the deionization time of thyratron number 4, type *2050*, is shown on Figure 16. The four curves plotted are to be compared by pairs. In each pair the

E_{v2} biases are different but have been adjusted to provide approximately the same trigger sensitivity.

The effect of ambient temperature on the deionization time of thyratron number 4, type *2050*, is shown on Figure 17.

The effect of the variation of pulse width (from 0.6 microsecond to 3.5 microseconds) upon the deionization time of the *2050* thyratron 4 is not shown. The variation of the pulse width over this range resulted in a slight increase in the deionization time of the tube for the wider pulses. The change in deionization time was so small that a single curve would represent the measurements if plotted on a scale similar to that of Figure 15. The precision of the measurement did not warrant a plot on an expanded scale.

The variation of deionization time of thyratron 2, type *2D21*, as a function of peak current, is shown on Figure 18. The peak current was varied by a procedure identical with that used in obtaining data for Figure 13.

The effect of variation of the ambient temperature on thyratron 2, type *2D21*, is shown on Figure 19.

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No Discussion

Performance of Pumped Ignitron Rectifiers

C. C. HERSKIND
MEMBER AIEE

E. J. REMSCHEID
MEMBER AIEE

A LARGE NUMBER of mercury-arc rectifiers of the pumped-ignitron type have been installed since 1940 in electrochemical, industrial, and railway service. During the peak of the war effort, mercury-arc rectifiers accounted for over 15 per cent of the industrial power consumption in this country. They still form quite an appreciable percentage of the industrial power load, and the number of new installations is growing.

Rectifiers have established notable records for reliable and trouble-free operation comparing favorably with other types of power equipment. These have been quite fully reported in a number of excellent papers recently presented by users. The purpose of this paper is to review some of the operating records which are available in order to set forth a more comprehensive picture of rectifier performance, considering both that obtained in service to date and that which can be realized in the future by making full use of recent improvements.

This survey reviews the performance of pumped ignitrons rated 1,000 kw and above, at 250 and 600 volts as applied for railway, electrochemical, and industrial service. It covers the rectifier and its immediate auxiliaries, such as, evacuating, temperature regulating, and excitation equipment.

The quality of the performance of any apparatus may be judged by its ability to provide continuous service, the materials and labor required to maintain it in good repair, and its life.

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C. C. Herskind and E. J. Remscheid are both with the power rectifier engineering department, General Electric Company, Schenectady, N. Y.

The development of equipment capable of attaining a high order of reliability is based on the following principles

1. The apparatus must be of such character that sudden failures are a very rare or infrequent occurrence, and progressive deterioration occurs at a low rate.

2. If sudden failure is to be avoided, the process of deterioration must be observed and measured so that failure may be anticipated and corrective action taken.

3. If low maintenance is to be realized, the rate and extent of deterioration must be minimized.

These principles are a useful guide in evaluating performance.

Performance Records

Operating records are usually sketchy and incomplete, as they are primarily descriptions of the troubles experienced. They may not cover all the factors which must be considered in a full evaluation of performance. In an endeavor to bring all the factors into a true perspective, the operating data was analyzed in accordance with the following outline

1. Duty—type of service; operating voltage; current loading; amount of phase control.

2. Reliability—interruptions (number of arc-backs); availability (time out of service).

3. Replacements—rectifier tanks (parts); rectifier tank reconditioning; auxiliaries.

4. Preventive maintenance—grade of maintenance.

Operating data for more than 20 installations totaling over 260 rectifiers were analyzed. Table I shows the duty on these rectifiers, and the record of interruptions caused by arc-back.

Duty

The duty on a rectifier depends primarily on the voltage, current, and the amount of phase control. Values for these factors are given in Table I. The duty imposed by the commutating action must also be considered in a complete description of the duty. Calculations show that in the installations covered by Table I, the commutating duty when operating with no phase control is approximately as shown in Table II.

Phase control increases both the initial inverse voltage and final commutation rate in the same ratio. For these rectifier installations, the use of phase control increases the commutation duty by the following amounts:

Phase control (% reduction in voltage)	5—10—15—20—25
Commutation duty (% increase)	30—50—65—78—90

The commutation duty may be reduced by the use of commutating reactors. One of the installations is so equipped.

The duty on the rectifier also depends on the magnitude and duration of fault currents to which it is subjected. These

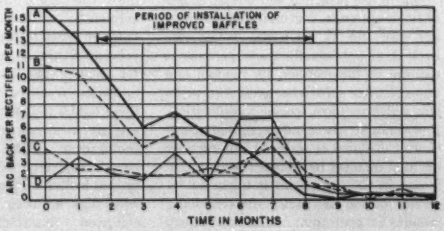

Figure 1. Improved operation resulting from installation of improved exhaust baffles in rectifiers on four potlines

Table I. Performance Records of Pumped Ignitrons

DESCRIPTION				DUTY				RECORD						
				RATING		LOADING		ARC BACK RATE AB/RECT/YR (1)						
INSTALLATION NUMBER	CLASS OF SERVICE	NO. OF UNITS	TANKS PER UNIT	DC VOLTS	DC AMPS (PER TANK)	% CURRENT	% PHASE CONTROL	1940	'41	'42	'43	'44	'45	'46
1	RAILWAY	1	12	625	416	50	0	21	1	0	0	0	5	4
2	"	5	12	625	416	50	0	5.2	3.8	3.4	2.6	5.8	3.0	1.8
3	"	1	12	600	416	50	0					2	2	0
4	ELECTROCHEM	12	12	645	416	90	0	435	5.3	6.1	1.0	3.3	1.8	1.3
5	"	16	6	640	666	98-106	0-5		26.1	12.7	16.7	13.4	18.1	
6	"	24	6	640	666	100	0-5		32.6	9.0	12.8	13.4	12.1	10.5
7	"	24	12	645	416	90	0			41.8	25.8	3.25	.35	.27
8	"	36	12	645	416	90	0			76.7	33.6	1.0		
9	"	16	6	640	666	100	0			13.3	23.4	15.9	6.7	
10	"	20	12	365	416	90	0				48			
11	"	8	6	580	486	103	0-10(2)			40.5	14.5	1.1	.15	0
12	"	48	12	650	416	90	0				54	6.8		
13	"	36	12	650	416	90	0				24.7	3.7		
14	"	12	12	600	416	90	0				39.0			
15	INDUSTRIAL	1	12	240	521	80	0-10							0
16	"	1	12	250	500	50	0-10			0	0	0	0	0
17	"	1	6	250	666	80	0-10					0	0	0
18	"	1	12	250	500	75	0-10							0
19	"	1	12	250	500	75	0-10							0

NOTES: (1) SERVICE PERIOD SHOWN BY UNDERSCORING
(2) UNIT EQUIPPED WITH COMMUTATING REACTORS

are determined by circuit constants and by the type of switchgear employed. No attempt has been made to cover this factor in the rectifier duty as the records of these installations indicate that adequate switchgear has been provided in so far as the rectifiers are concerned.

The class of service is another factor in duty. In railway service, the average current loading is usually less than a half of the continuous rating, but the rectifier is subjected to short time overloads of considerable magnitude. In electrochemical service, the load is constant and approaches the continuous rating of the unit. Industrial rectifiers carry a variable load having an average value about three-quarters of the continuous rating and occasional overloads of moderate value.

Reliability

The reliability of a rectifier may be measured by the number of interruptions in service arising from sudden failure, and the percentage of time it is available for service.

One of the principal and unique causes of interruption in continuity of the rectifier operation is arc-back. While arc-back does not usually cause permanent damage to the rectifier, its random and unpredictable occurrence presents a problem which requires special consideration. For example, where uninterrupted operation of d-c load is required, the rectifier installation must be designed so that only a part of the equipment is taken out of service during arc-back; the remainder remains in operation. While continuity of operation is obtained despite random arc-back, practical considerations require a low arc-back rate, as frequent arc-backs increase the duty on transformers and switchgear, thereby increasing the probability of their failure and shortening their life. Maintenance of circuit breakers is also increased. Furthermore, the random and unpredictable occurrence of arc-back increases the problems of the operating staff and the probability of mistakes in switching.

Arc-back rates for 19 installations are given in Table I. The period of operation covered by these records is shown by the heavy line underscoring each record. Where no values are given during the operating period, the records are not available.

The high arc-back rate experienced on some of the electrochemical installations during initial operation was caused by a faulty design which permitted liquid mercury to flow out of the exhaust line and strike the anode. Extensive field and factory tests were made in order to determine the cause of the arc-backs and the trouble was finally diagnosed in the middle of the year of 1943. The trouble was corrected by installing an improved baffle in front of the exhaust opening to prevent returning mercury from striking the anode. The drastic improvement in operation resulting from installation of these baffles is shown by Figure 1, which present a record obtained on a large installation. Some installations have been completely fitted with these improved baffles. On others, only the tanks with high arc-back rates have been so equipped. As a result, these latter installations show a higher arc-back rate than could otherwise actually be realized.

The arc-back records on the rectifiers in industrial service are incomplete. While no arc-backs are indicated in Table I, some have occurred during the initial operation of some of these units. However, arc-backs are so infrequent and are usually of so little consequence that no effort is made to keep accurate records.

The arc-back records given here probably tend to be pessimistic since full records are usually kept on units giving trouble while units which are operating without incident are forgotten. High arc-back rates are, of course, undesirable and tend to become a prime factor in rectifier performance. When the arc-back rate is below 2 or 3 per year per rectifier, it becomes a relatively unimportant factor and maintenance costs assume a dominant role.

From the point of view of reliability, arc-back stability is an important consideration when a rectifier is operating with a high arc-back rate. If the arc-back rate is not stable but tends to increase under such conditions, the unit may rapidly become inoperative. Experience has shown that pumped rectifiers may be operated with a high arc-back rate without rapid progressive deterioration. When one of the large installations reported here was put first into service, arc-backs occurred at a rate exceeding one arc-back per rectifier per day. Various causes contributed to this high rate, perhaps the most important of which was the presence of fine ceramic particles. The trouble was soon corrected and the installation has since operated with a very low arc-back rate.

Availability

There is no standard procedure for determining availability and so it does not therefore, furnish an accurate measure of rectifier performance. Availability is generally defined as the percentage of elapsed time that the unit is available for service. However, different users have different maintenance procedures and operating practices. Some users never take the unit out of service unless required for repairs while others have a definite maintenance procedure which may take the unit out of service for a considerable percentage of time. Reports on some rectifiers in electrochemical and industrial service claim 100 per cent availability. On the other hand, an availability of 92 per cent has been reported for some units in railway service which are giving good performance but are

taken out of service for long periods of time to fit a work schedule.

Replacements

It is difficult to determine accurately the annual replacements for the average rectifier unit. Reports from different installations vary widely with some showing operation of a single rectifier for several years with no replacements, while others indicate unusual failures not previously experienced. All reports indicate that the number and cost of replacements is very low.

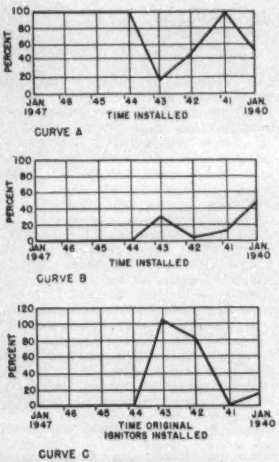

Figure 2. Ignitor performance records

Curve *A*—Percentage of installed ignitors still in service

Curve *B*—Percentage of installed ignitors which failed in service

Curve *C*—Percentage of installed ignitors replaced by improved ignitors before failure

Reports for some electrochemical installations indicate total annual replacements for 12 rectifier units consisting of a few small components, such as, water pump bearings, heaters, electrolytic leads, and excitation circuit components (reactors, capacitors, and rectifiers). Failures of major items such as tanks, seals, and vacuum pumps occur but rarely.

Ignitors

Data covering ignitor performance are shown on Figure 2. These data were obtained from records covering over 800 ignitors installed in rectifiers operating in all three classes of service. The records show a large number of ignitor replacements but the causes of trouble have been corrected and the high replacement rate is not expected to recur.

Referring to Figure 2, curve *A* shows the percentage of ignitors installed during each of the past seven years which are still in service. All ignitors installed during the past three years are still in service. Many of the ignitors installed four and five years ago have been replaced with new ignitors of improved design. Practically all of the ignitors which were installed six years ago were in rectifier applications involving moderate duties. The curve shows that most of these are still in operation.

Curve *B* shows the percentage of ignitors which actually failed after the respective years in operation. Ignitors of the improved design have all survived three years of operation.

Curve *C* shows the percentage of ignitors replaced by new ignitors of improved design. These ignitors did not fail in service.

Poor ignitor performance was experienced on some of the large electrochemical installations. Studies indicated two causes of failure. One of these was the loosening of the ignitor in its metal support. Since the ignitor operates at 900 degrees centigrade, this mount must be designed to withstand high temperatures. The mechanical troubles were overcome by the substitution of suitable metals having an expansion coefficient near that of the ignitor material. The other was the wetting of ignitors by aluminum and other metals dissolved in the mercury. This was traced to the starter gasket. The source of wetting material was found to be the aluminum gasket at the starter opening. This trouble was corrected by replacing the inner aluminum gasket by a gasket of plastic material (Formar) which is not attacked by the mercury. The records show that no ignitor failures have occurred since the installation of the improved ignitors and gaskets. The ignitor and gasket changes were made at the time of installation of the improved exhaust baffle.

Reconditioning

The necessity for and the frequency of regular reconditioning of pumped ignitrons has not yet been fully established. The present practice is to recondition rectifier tanks when their operating records show an increasing or undesirable arc-back rate.

At the present time, reconditioning is not fully defined and as a result it is difficult to evaluate data from operating records. For example, it is possible to replace an ignitor or install a splash baffle in a pumped ignitron tank in less than one hour and put the tank back in service without degassing. As only one small part has been changed and the tank has not been degassed, this should not be considered as a reconditioning operation.

Since reconditioning of a rectifier tank requires appreciable time and may be an important item in maintenance costs, it is proposed that it be defined as follows: Reconditioning of a rectifier tank is a complete overhaul followed by degassing. A typical reconditioning procedure consists in opening the tank by removing the cover and starter, checking all internal parts, replacing any which are defective, replacing the ignitor if it shows evidence of wetting or erosion, brushing down the tank walls, replacing the old mercury with new clean mercury, installing new gaskets, replacing cover and starter, and finally degassing the tank.

The operating records indicate that pumped ignitrons incorporating the latest improvements can be expected to operate over 5 years without reconditioning. Further operating experience must be obtained to determine the average service life before reconditioning.

Corrosion

Pumped ignitrons are almost invariably provided with a recirculating cooling system using water for the cooling medium. Where the rectifier tanks and their water jackets are of steel, corrosion may be a

Table II

Service	D-C Volts	D-C Amperes Per Tank	Initial Inverse Volts	Final Commutation Rate Amperes Per Micro-Second
Railway	600	416	500	3
Electrochemical	600	416	600	2
Electrochemical	600	666	600	3
Industrial	250	500	250	3

serious menace to the life of the equipment when raw water is used as a coolant. It has been found that corrosion can be eliminated by adding less than 1/2 of 1 per cent of anhydrous sodium chromate to the recirculating water. Operating records covering more than 15 years show that when sodium chromate is properly applied and maintained, corrosion is effectively prevented.

Preventive Maintenance

Preventive maintenance is a program or the prevention of sudden failure by the maintenance of favorable conditions and the control of deterioration. It is based on occurrence of deterioration as a gradual process subject to observation. Since inadequate maintenance affects the condition of the equipment and the probability of failure, performance is dependent in some degree on the grade of maintenance.

A thorough maintenance program is standard practice on large installations as there is usually sufficient work to require the full time of a small crew. In small installations with one or two rectifiers, maintenance is usually done in a less systematic manner as it requires only part time of the maintenance crew and is done at times when other duties permit.

Summary

Operating records indicate that rectifiers equipped with the latest improvements can be expected to perform as follows:

1. Arc-back rate of less than 1 or 2 arc-backs per rectifier per year.
2. Average ignitor life exceeding 5 years.
3. Service life on rectifier tank exceeding 5 years, without reconditioning.

No Discussion

Railroad Electrification Energy Conversion and Transmission Costs

R. L. KIMBALL
ASSOCIATE AIEE

J. G. HOLM
MEMBER AIEE

Synopsis: Railroad electrification is now being considered in several sections of the United States. Questions regarding choice of power supply, design and cost have prompted an analysis of these problems on a two-track railroad of moderate traffic density. The scope covers features from utility supply to locomotive pantograph, including conversion apparatus, substations, and transmission. Locomotives are not included. A 330-mile section of railroad is used with trackage and traffic density closely resembling an actual case. A-c and d-c power systems are analyzed and compared. The a-c system is single-phase 25-cycle, with 24-kv trolley and 72-kv feeder-to-trolley voltages. Rotating frequency changer sets are used for conversion, supplying loads resulting from motive power equipped with single-phase motors, with or without power-factor corrective equipment, or with motor generators. The cost of conversion apparatus is considered separately, as it might be financed either by the railroad or utility. The d-c system uses 6-kv trolley voltage, with supplementary feeders. Metal-tank mercury-arc rectifiers are provided for conversion. Wood poles support all electrical circuits in both cases. Traffic densities are considered for 1940 and 1944 levels, the latter 57 per cent greater than the former. Cost estimates are based on 1939 price levels. The results show:

1. Traffic density is one of the controlling factors of cost of railroad electrification, but reasonable cost per annual thousand gross ton-miles can be obtained for a railroad handling moderate traffic.

2. The conversion and transmission requirements of electrification, carefully engineered, result in an annual cost of approximately 40 cents per thousand gross ton-miles, varying somewhat with the density of traffic.

3. A-c and d-c power systems are economically comparable on an electrification such as that studied herein.

4. The information presented herein is believed to be sufficient for making comparisons with present railroad operating costs and for assisting railroad management in estimating the value of electrification as a solution to the many complex problems that are demanding attention today.

5. There is no general solution to a railroad electrification problem; a thorough analysis of each particular case is required.

POSSIBLE lower electric power cost relative to fuels now used for motive power is directing considerable attention to additional railroad electrification in the United States. With the already recognized economies and advantages of electrification, coupled with the increase in economies by lowered electric power cost and reduction in capital cost by design consistent with conditions, electrification becomes increasingly attractive. Although economic comparisons between electrification and dieselization are beyond the scope of this paper, the relative trend of energy and fuel costs, just referred to, as well as the desirability of conserving rapidly diminishing national resources, favor electrification. This should give railroad management a stimulus to undertake studies of its possibilities.[1]

For illustration, an analysis has been made of a theoretical railroad handling moderate traffic which is representative of a number of nonelectrified railroads in the United States. Cost comparisons are based upon suitable designs of reasonably low capital cost. Electric energy is assumed to be purchased.[2] To the annual costs derived from this analysis must be added the cost of purchased electric power, which has not been included and which varies from four to six mills per kilowatt-hour at 60 cycles depending on local conditions and forms of contract. The economic results obtained are not presented as a generalized solution of the broad problem, but indicate the magnitude of capital and annual costs. In an actual case of a proposed railroad electrification, detailed study of the particular problem must be made. From the data presented, however, comparisons can be made with present operating costs.

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R. L. KIMBALL and J. G. HOLM are both with Gibbs and Hill, Inc., New York, N. Y.

In the preparation of this paper extensive use has been made of data and experience accumulated by Gibbs and Hill, Inc., in past railroad electrification. Due acknowledgment is made to the clients of Gibbs and Hill, Inc., and to the manufacturers of electrical apparatus that have contributed information.

SCOPE

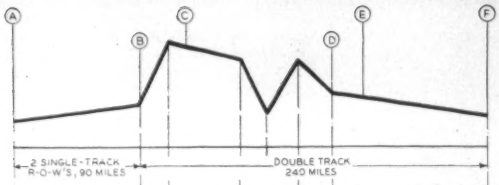
Either a-c or d-c electrification has advantages compared with other motive power and, therefore, both are considered. The a-c system is 25-cycle single-phase, with rotating frequency changers to convert from the 60-cycle energy normally supplied by a utility. These conversion units supply power to either of two types of motive power: equipped with single-phase series motors, operating at 85 per cent power factor lagging; or with either motor-generator sets operating at 95 per cent power factor leading, or series motors whose power factor is corrected sufficiently to obtain a similar leading power factor. The two cases are identified as 85 per cent lagging and 95 per cent leading without reference to the particular type of locomotive. The d-c system uses metal-tank mercury-arc rectifier units to supply traction requirements from corresponding 60-cycle sources.

A 24-kv trolley voltage for a-c and 6-kv for d-c electrifications have been selected. They are higher than voltages now in general use, but are more moderate than the 48-kv for a-c and 15-kv for d-c systems that have been considered. This selection is justified by operating experience in the case of 24-kv a-c and by general progress in the design of d-c equipment.

For each system two traffic densities are assumed corresponding to 1940 and 1944 levels designated 100 per cent and 157 per cent traffic, respectively.

Costs are developed for each system on 1939 prices which are considered the most recent stable basis for comparison.

Cost of signal changes, electrolysis mitigation, changes in alignment road-

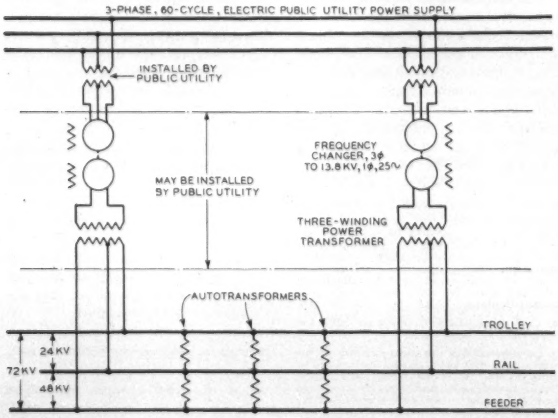


DISTANCE, MILES	120		120				90
DISTANCE, MILES	90.0	14.7	50.3	11.8	24.7	23.5	115.0
COMPENSATED GRADE RESISTANCE A TO F	+2.4	-15.8	+0.4	-14.0	+9.0	-3.0	+0.8
COMPENSATED GRADE RESISTANCE F TO A	+0.8	-12.6	+2.8	+17.2	-5.8	+6.2	+2.4
OPERATING SPEED MPH A TO F	55	30	60	60	40	60	50
OPERATING SPEED MPH F TO A	60	60	55	30	60	45	45
NO. OF CABS IN LOCOMOTIVE	3	3	3	3	3	3	2
TROLLEY KW AT TRAIN	7490	–	7520	7580	3270	7040	5030

Figure 1. Railroad profile. Trackage data and motive-power performance characteristics

way, bridge, and tunnel clearances have not been included because they cannot be generalized. Provision for inductive coordination has been included to the extent of eliminating a-c stub-end feed under fault conditions and of providing at least a normal 24-phase d-c rectifier operation. To simplify analysis, expenditures for changes in such existing facilities have not been evaluated in the cost estimate. On the other hand, this study does not include major savings obtained from electric operation and the intangible factors of appeal. These savings, shown by experience to be of the order of 67 per cent reduction in locomotive maintenance when compared with steam operation, 25 per cent reduction in train and engine-crew costs, 75 per cent reduction in engine-house expense; together with smaller number of locomotives required, practical elimination of cost of water and maintenance of fuel and water stations, decreased maintenance on wheels and brake shoes due to advantages secured by regeneration, and absence of possible smoke and diesel-fume abatement expenditures, more than compensate for the expenditures omitted from costs, and are, in fact, the elements making railroad electrification economically attractive.

Figure 2. Diagram of electric supply circuit. A-c electrification



Description of Trackage Electrified

The theoretical 330-mile railroad chosen for analysis, and shown in profile in Figure 1, has trackage and traffic conditions closely resembling an actual steam-operated road. Average grades are approximately 0.8 per cent, with maximum and ruling grades of 1.2 per cent. Average curvature is approximately two degrees. The profile and compensated grade resistance in both directions of traffic are also shown on the figure. The total track mileage, laid with 115-pound rail, consists of 136 miles of single track (92 miles of which are two parallel single tracks on separate rights-of-way), 240 miles of double track, 44 miles of three track, and 45 miles of trackage in yards.

Character of Traffic and Characteristics of Motive Power

Total annual traffic density is taken at 6,065,600 thousand gross ton-miles at 100 per cent traffic and 9,524,000 thousand gross ton-miles at 157 per cent. These figures consist of 947,000 thousand gross ton-miles of passenger and 5,118,500 frieght at 100 per cent and 1,093,600 passenger and 8,431,000 freight at 157 per cent. In the analysis, the passenger and freight gross ton-miles are combined and treated together. The maximum number of daily freight trains to handle this traffic in each direction is 13 and 20 for the two traffic levels, when the ratio of maximum to average daily traffic is 1.5. The proportion of traffic moved in each direction is fairly well balanced in each section. The annual freight tons hauled from F to B are approximately 80 per cent of the freight hauled from B to F and the tons moved between A and B are the same in each direction. Passenger traffic is the same in both directions, six trains being scheduled each way.

Substation apparatus ratings and hourly power demands are based on fleets of trains moving simultaneously in both directions. For 100 per cent traffic each of these fleets consists of four trains, and for 157 per cent—of five trains. In both cases the spacing between trains is 18 miles and the fleets are preceded and followed by uniform train movements every 40 miles. Thus "bunching" of trains for the two traffic levels takes place over 54 and 72 miles, respectively. Because the system must be designed to handle peak movements, an advantage

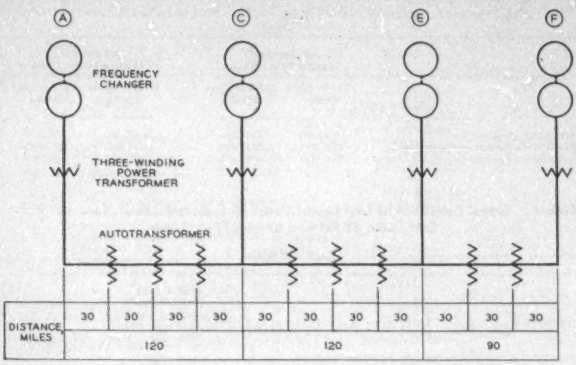


Figure 3. Schematic diagram of substation spacings. A-c electrification

accrues to electrification in having sufficient capacity to handle increased traffic represented by added trains between peak movements. This is one of the reasons electrifications have so successfully handled unexpected traffic increases, such as have occurred during wartime.

In freight operation the rating of the interchangeable freight and passenger electric locomotives is 3,600 tons, representing a 30 per cent train consolidation compared with the usual steam operation for this traffic.

Alternating-Current Electrification

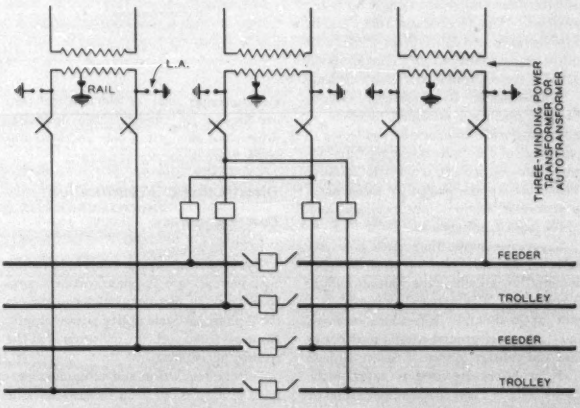
Electric Supply Circuits and Substation Spacing

The electric power conversion apparatus comprises rotating frequency changers installed at main substations fed through step-down transformers from the 3-phase 60-cycle utility power-supply lines. These conversion stations in turn feed 3-winding, single-phase 25-cycle power transformers supplying the feeder and trolley circuits. Autotransformer stations, connecting the trolley and feeder with the rail, are placed between main substations to maintain 24 kv on the trolley and 72 kv on the feeder circuit. This is shown in Figure 2.

This design eliminates the necessity of separate single-phase transmission lines paralleling the track and uses the autotransformers to obtain the desired voltage between trolley and rail.

The distance between autotransformer stations is theoretically determined by the allowable voltage drop in feeder and trolley circuits. Studies of 20, 30, 40, 50, and 60-mile spacings indicate that 30-mile spacing provides a reasonable margin in voltage drop for the location of autotransformer stations adjacent to crossovers or interlocking plants, and insures voltages above the 18-kv minimum considered advisable. With the 30-mile spacing and the feeder and trolley sizes used, the minimum delivered voltage at the locomotive is slightly over 20 kv. With 95 per cent power factor leading, a delivered voltage in excess of 21 kv is obtained with all autotransformer spacings. However, economic considerations and operating requirements, such as the maximum desirable length of track out of service at time of fault, indicate the choice of 30-mile spacing for both cases. A schematic diagram is shown in Figure 3.

Figure 4. Arrangement of switching equipment at substations. A-c electrification



Electric Power Requirements and Apparatus

Demands per train measured at the trolley on the A to D and D to F sections of the road are 7,500 and 5,000 kw, respectively, indicated in Figure 1, on which operating speeds are also shown. Load curves for the assumed train movements show maximum 1-hour average demands per conversion station of approximately 60,000 and 73,000 kw for the two levels of traffic. The combined maximum one-hour average demands for the system are approximately 199,000 and 218,000 kw, respectively. Proper allowances for efficiency, power factor, and varying load demands are made in determining the size of power apparatus. Spare capacity of each type of apparatus is provided in the conversion stations and at each autotransformer station. All transformers are capable of operation at 150 per cent load for two hours, and frequency changers can operate continuously at 115 per cent of nominal rating without further sustained overload capacity.

As the train fleet represents a moving load whose peak demand passes from one main substation to another, the total kilowatt capacity of frequency changers is reduced by interchange of power between adjacent conversion stations. At times of outage of a frequency changer the deficiency in capacity at that location is met by the adjacent conversion station on one or both sides. The simultaneous load on such conversion stations adjacent to a peak-loaded station is conservatively taken at 75 per cent of their own maximum peaks. This transfer is restricted to kilowatt flow, with practically firm reactive capacity being provided in each conversion station. Under this condition,

with both feeder systems in operation, no excessive voltage drop occurs.

The electrical apparatus required for the entire a-c electrification consists of 12 frequency changers, 13 3-winding power transformers and 24 autotransformers. Although the number of units remains the same for the various conditions of traffic and power factor of the load, the total installed capacity of each varies as shown in Table I.

Trolley-Feeder Circuits and Supporting Structures

Two separate trolley-feeder circuits are provided between adjacent substations, at each of which the circuits are sectionalized. The trolley system consists of a messenger of 400,000 circular mils copper equivalent with a contact wire of 300,000 circular mils 80 per cent equivalent conductivity. The feeders, capable of handling the entire traffic load with one feeder out of operation, are 350,000 circular mils and 250,000 circular mils for the 85 per cent lagging and 95 per cent leading cases, respectively.

The diagram in Figure 4 shows the arrangement of circuit breakers in the trolley-feeder circuits at main substations. A similar arrangement is employed at autotransformer stations. At station *A* where four power transformers are required, two are located on each side of the tie-circuit breakers, one per trolley-feeder circuit.

During normal operation the capacity of all transformers is available to any or all trolley-feeder circuits. This connection of feeder and trolley circuit breakers, in conjunction with a power-transformer primary circuit breaker where necessary, permits de-energizing any transformer and disconnecting it by means of horn-gap switches. Upon loss of one trolley and its load between two substations, 75 per cent of the transformer capacity remains available for the reduced load. Fault clearing is accomplished by automatic circuit-breaker operation arranged to leave an autotransformer temporarily connected to each end of the faulted circuit. In this way stub-end feed to a fault is avoided. Phase breaks are placed at three autotransformer stations, one between each of the main supply points.

Experience shows that wood poles are suitable for this type of electrification, materially reducing the initial capital investment. Each structure is a class 3 pole, approximately 50 feet long, carrying one 1/0 copper ground wire, a feeder wire and the contact system. Seven 10-inch disk insulators are used to support the feeder wire. There are assumed to be 31 structures per mile, of which 40 per cent are located on tangent. No provision, other than strength of pole and available space, is made for signal circuits.

Table I

	85 Per Cent Lagging Power Factor		95 Per Cent Leading Power Factor	
	100 Per Cent Traffic	157 Per Cent Traffic	100 Per Cent Traffic	157 Per Cent Traffic
Frequency changers kw	215,400	239,250	215,400	239,250
Power transformers primary kva	273,500	324,500	219,500	264,000
Autotransformers kva	288,000	288,000	240,000	240,000

Table II. Typical Calculation of Total Capital Cost. A-C Electrification. Case of 157 Per Cent Traffic, 85 Per Cent Lagging Power Factor

(Cost in dollars)

Item Description	Cost of Item (Installed on foundation where needed including labor cost)	Group Cost
1. Main Substations (total for four)		
1.1 Land	22,700	
1.2 Structures (including buses, insulators, supports)	270,000	
1.3 Frequency conversion apparatus	5,681,300	
1.4 Three-winding power transformers	1,065,000	
1.5 High-voltage circuit breakers	222,600	
1.6 High-voltage air-break disconnectors	74,800	
1.7 High-voltage protective and control apparatus	90,000	
1.8 Low-voltage switching, protective, and control apparatus (including voltage regulators)	675,000	
1.9 Auxiliary substation apparatus	180,000	
	8,281,400	
Indirect expenses	1,863,300	
Cost of money during construction	507,200	10,651,900
2. Autotransformer Substations (total for eight)		
2.1 Land	5,800	
2.2 Structures (including buses, insulators, supports)	328,000	
2.3 Autotransformers	1,062,900	
2.4 High-voltage circuit breakers	445,300	
2.5 High-voltage air-break disconnectors (including phase breakers)	144,400	
2.6 High-voltage protective and control apparatus	109,500	
2.7 Auxiliary substation apparatus	218,000	
	2,313,900	
Indirect expenses	520,600	
Cost of money during construction	141,700	2,976,200
3. Feeder-Trolley Circuits—Main Line and Yards		
3.1 Single-track	1,636,100	
3.2 Two-track	5,832,000	
3.3 Three-track	1,405,800	
3.4 Yard-track	315,000	
	9,188,900	
Indirect expenses	2,067,500	
Cost of money during construction	562,800	11,819,200
Total Capital Cost		25,447,300

Direct-Current Electrification

Electric Supply Circuit and Substation Spacing

Conversion apparatus consists of metal-tank mercury-arc 12-phase rectifiers, supplied through rectifier transformers from the 3-phase 60-cycle utility power supply. Nominal 6,000 volts are maintained at the rectifier terminals.

Voltage regulation and economics permit the location of main rectifier stations at 60-mile spacing with end substations located 15 miles from each terminal. This substation spacing, with the selected feeder and trolley conductivities, provides a minimum of approximately 4,650 volts at the train with maximum traffic concentration, corresponding to an average voltage drop of about 11 per cent. The two supplementary feeder and trolley systems are connected electrically at the quarter- and mid-points between substations by intermediate tie stations having a common bus, as shown in Figure 5.

Power Requirements and Apparatus

Power requirements are estimated to be 7,040 and 4,700 kw input to the train on

Table III. Typical Calculation of Total Capital Cost. D-C Electrification. Case of 157 Per Cent Traffic

(Cost in dollars)

Item Description	Cost of Item (Installed on foundation where needed including labor cost)	Group Cost
1. Main Substations (total for six)		
1.1 Land	13,350	
1.2 Structures (including buses, insulators, supports)	172,500	
1.3 Current-conversion apparatus (rectifiers, rectifier transformers, including high-voltage circuit breakers and high-voltage protective and control apparatus)	10,736,000	
1.4 Phase shifters	202,000	
1.5 Auxiliary substation apparatus	115,000	
	11,238,850	
Indirect expenses	2,528,750	
Cost of money during construction	688,400	14,456,000
2. Intermediate Tie Substations (total for 17)		
2.1 Land	1,800	
2.2 Structures (including buses and supports)	20,400	
2.3 High-voltage sectionalizing circuit breakers (including disconnects and protective and control apparatus)	517,650	
2.4 Auxiliary substation apparatus	25,500	
	565,350	
Indirect expenses	137,200	
Cost of money during construction	34,650	727,200
3. Feeder-Trolley Circuits—Main Line and Yards		
3.1 Single-track	1,978,100	
3.2 Two-track	7,362,700	
3.3 Three-track	1,645,600	
3.4 Yard-track	315,000	
	11,301,400	
Indirect expenses	2,542,800	
Cost of money during construction	692,200	14,536,400
Total Capital Cost		29,719,600

the A to D and D to F sections of the profile respectively.

Train movements, previously described, require 165,000 kw of rectifier capacity for 100 per cent traffic and 178,000 kw for 157 per cent. The same size units are used at each substation, 7,500 kw for 100 per cent traffic and 8,000 kw for 157 per cent; and there are four units at each substation, except at e and f where there are three.

The maximum one-hour average demand is approximately 146,000 kilowatts for 100 per cent traffic and 150,000 kilowatts for 157 per cent.

The substations include phase shifters for basic wave-shape correction which will presumably meet all except the most unusual requirements of inductive coordination.

Trolley and Feeder Circuits and Supporting Structures

Similarly to the a-c electrification, two separate trolley and supplementary feeder circuits are provided. The trolley system consists of a messenger of 500,000 circular mils copper equivalent and of two contact wires of 300,000 circular mils 80 per cent equivalent, per track. Each supplementary feeder is 1,000,000 circular mils copper.

The arrangement of circuit breakers at the main and intermediate tie substations is shown in Figure 6. No phase breaks are required.

The wood-pole supporting structures are the same as those used in a-c electrification, except that heavier feeder and catenary systems require a class 2 pole. The same 1/0-size copper ground wire is used.

Cost Comparisons

Method of Estimating Capital Cost

For estimating the capital cost of the conversion and transmission requirements of electrification, the system is divided into the following groups:

1. Main substations
2. Autotransformer substation, or tie substations
3. Feeder and trolley circuits and supporting structures, including rail bonding

Typical calculations of capital cost for both a-c and d-c systems are given in Tables II and III, respectively.

The cost of all apparatus and material delivered to the site is developed from manufacturers' estimates. To these costs are added percentages covering labor, construction-equipment rental, and indirect expenses. The labor percentages were determined from experience on actual electrifications and vary with the type of apparatus. Indirect expenses cover the following: engineering expenses, field superintendence, legal expenses, taxes, insurance during construction, and normal contingencies. To cover indirect expenses, $22^1/_2$ per cent is added to the delivered and erected costs. Money is taken at four per cent for $1^1/_4$ years which is one half of the assumed construction period. This method of cost estimating is described in greater detail in a paper written by one of the authors.[3] Should it be desirable to modify either the indirect expenses or the length of the construction period, changes can be introduced easily in the capital cost figures.

Capital Cost of A-C Electrification

Total capital costs of the four cases of a-c electrification are given in Table IV.

These costs are separated into the three major divisions in Table V.

The value of locomotive power factor improvement is readily seen. The effect of traffic density is also evident, the 57 per cent greater traffic requiring an increase of only one per cent in total capital cost which in turn produces a decrease of about 36 per cent in cost per thousand gross ton-miles. This is because the maximum concentration of trains which may occur does not differ greatly for the two annual traffic conditions.

In some instances the utility is willing to assume the financing of the conversion stations in order to reduce the railroad's capital investment. The cost of such conversion stations accounts for approximately 40 per cent of the total cost of the power system, as shown in Table VI.

If it is assumed that the utility will

Table IV

	85 Per Cent Lagging Power Factor		95 Per Cent Leading Power Factor	
	100 Per Cent Traffic	157 Per Cent Traffic	100 Per Cent Traffic	157 Per Cent Traffic
Total	$25,198,200	$25,447,300	$23,118,400	$23,310,600
Per route-mile	76,300	77,100	70,000	70,550
Per track-mile	33,700	34,000	30,900	31,100
Per thousand gross ton-miles	4.15	2.67	3.81	2.45

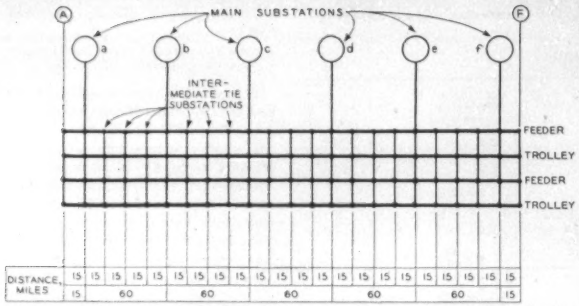


Figure 5. Schematic diagram showing location of rectifier and intermediate tie substations. D-c electrification

install the conversion stations, the total capital expenditure of the railroad will be reduced accordingly but at the expense of the increased cost of purchased power.

Capital Cost of D-C Electrification

Total capital cost of the d-c electrification is given below:

	100 Per Cent Traffic	157 Per Cent Traffic
Total	$28,819,500	$29,719,600
Per route-mile	87,300	90,050
Per track-mile	38,500	39,700
Per thousand gross ton-miles	4.75	3.12

These costs are separated into the three major divisions as shown in Table VII. Cost of rectifier apparatus installed is assumed at $61 per kilowatt of capacity, including rectifiers, transformers, high-voltage circuit breakers, and protective and control apparatus. This figure does not include the costs of foundations and indirect expenses which have been added separately.

Similar to the a-c electrification, the effect of traffic density is evident, 57 per cent greater traffic requiring an increase of only three per cent total capital cost, which in turn produces a decrease of about 34 per cent in cost per thousand gross ton-miles. If it is assumed that the utility will install the main substations, that is, the rectifiers and transformers, switching and control apparatus, the total capital expenditure of the railroad may be reduced by about 48 per cent, to $13,555,900 and $14,456,000 for the two levels of traffic. Here again, this reduction is at the expense of the cost of purchased power.

Method of Estimating Annual Cost

Annual cost (exclusive of cost of purchased power) is the sum of annual fixed charges and annual operating expenses. Annual fixed charges cover taxes, insurance, depreciation, and interest on money invested. Annual operating expenses cover maintainance, operation, and cost of energy loss. Both annual fixed charges and annual expenses for maintenance and operation are calculated as percentages of capital cost for each of the three major groups into which the capital cost structure is divided. These percentages are the same for both a-c and d-c systems. The annual fixed charges amount to eight per cent, made up of one per cent for taxes, one-half of one per cent for insurance, two and one-half per cent for depreciation (on the basis of 40-year average useful life of apparatus) and four per cent for interest on money. The annual expense for maintenance and operation is two per cent for main substations, autotransformer and intermediate tie substations; it is three per cent for trolley and feeder circuits. Losses in rectifiers are considerably smaller than in frequency changers with their 3-winding transformers, for which an adequate allowance has been made. Comparisons are made for annual load factors of 30, 45, and 60 per cent.

Annual Cost of A-C Electrification

Annual costs derived from the capital costs given previously, but not including the cost of electric energy or annual locomotive costs, are as shown in Table VIII.

These costs are separated into the three major divisions as shown in Table IX.

The saving in capital cost by improving the power factor from 85 per cent lagging to 95 per cent leading is reflected here in a 10 per cent decrease in the annual charges.

Annual cost of the system necessary for the larger traffic is only slightly higher than that necessary for 100 per cent traffic but the annual cost per thousand gross

Table V. Capital Cost. A-C Electrification

(Cost in dollars)

Item Description	85 Per Cent Lagging Power Factor		95 Per Cent Leading Power Factor	
	100 Per Cent Traffic	157 Per Cent Traffic	100 Per Cent Traffic	157 Per Cent Traffic
Entire System				
Total cost	25,198,200	25,447,300	23,118,400	23,310,600
Cost per route-mile	76,300	77,100	70,000	70,550
Cost per track-mile	33,700	34,000	30,900	31,100
Cost per 1,000 gross ton-miles	4.15	2.67	3.81	2.45
Main Substations				
Total cost	10,402,800	10,651,900	8,937,600	9,129,800
Cost per kw of installed frequency changers	48.3	44.5	41.5	38.2
Cost per 1,000 gross ton-miles	1.71	1.12	1.47	0.96
Autotransformer Substations				
Total cost	2,976,200	2,976,200	2,734,800	2,734,800
Cost per kva of installed autotransformers	10.33	10.33	11.40	11.40
Cost per 1,000 gross ton-miles	0.49	0.31	0.45	0.29
Feeder-Trolley Circuits—Main Line and Yards				
Total cost	11,819,200	11,819,200	11,446,000	11,446,000
Cost per route-mile	35,800	35,800	34,700	34,700
Cost per track-mile	15,800	15,800	15,300	15,300
Cost per 1000 gross ton-miles	1.95	1.24	1.89	1.20

Table VI

	85 Per Cent Lagging Power Factor		95 Per Cent Leading Power Factor	
	100 Per Cent Traffic	157 Per Cent Traffic	100 Per Cent Traffic	157 Per Cent Traffic
Conversion stations	$10,402,800	$10,651,900	$8,937,600	$9,129,800

ton-miles for the higher traffic is approximately 35 per cent lower. In other words, the energy conversion and transmission costs of an electrification do not vary directly with annual traffic because they are determined by the maximum concentration of movement and not by the average.

Should the utility assume the financing of the conversion stations in order to reduce the railroad's capital investment, the resulting annual interest payments by the railroad will decrease by approximately 40 per cent, at the expense of an increased cost of purchased power.

Annual Cost of D-C Electrification

Annual costs of d-c electrification have been determined in the same manner as

Table VII. Capital Cost. D-C Electrification

(Cost in dollars)

Item Description	100 Per Cent Traffic	157 Per Cent Traffic
Entire System		
Total cost	28,819,500	29,719,600
Cost per route-mile	87,300	90,050
Cost per track-mile	38,500	39,700
Cost per 1,000 gross ton-miles	4.75	3.12
Main Substations		
Total cost	13,555,900	14,456,000
Cost per kw of installed rectifiers	82.2	82.1
Cost per 1,000 gross ton-miles	2.24	1.52
Intermediate Tie Substations		
Total cost	727,200	727,200
Cost per 1,000 gross ton-miles	0.12	0.076
Feeder-Trolley Circuits—Main Line and Yards		
Total cost	14,536,400	14,536,400
Cost per route-mile	44,050	44,050
Cost per track-mile	19,450	19,450
Cost per 1,000 gross ton-miles	2.40	1.53

those of the a-c electrification and are as follows:

	100 Per Cent Traffic	157 Per Cent Traffic
Total per year	$3,456,100	$3,547,300
Per kwhr used at		
30% annual load factor	9.02 mills	8.97 mills
45% annual load factor	6.02 mills	5.98 mills
60% annual load factor	4.51 mills	4.48 mills
Per route-mile per year	$10,450	$10,750
Per track-mile per year	4,620	4,740
Per thousand gross ton-miles per year	0.569	0.372

These costs are also separated into the three major divisions as shown in Table X.

As in the a-c electrification the annual cost of the system for the larger traffic is only three per cent greater than for 100 per cent traffic but the annual cost per thousand gross ton-miles for the higher traffic is approximately 35 per cent less.

Utility financing of the main substations would reduce the annual interest payments by approximately 45 per cent at the expense of an increased cost of purchased power.

For 157 per cent traffic and assuming that the railroad finances the entire electrification, the capital costs per thousand gross ton-miles vary as follows:

A-c electrification 95 per cent leading power factor $2.45

A-c electrification 85 per cent lagging power factor $2.67

D-c electrification $3.12

Table VIII

	85 Per Cent Lagging Power Factor		95 Per Cent Leading Power Factor	
	100 Per Cent Traffic	157 Per Cent Traffic	100 Per Cent Traffic	157 Per Cent Traffic
Total per year	$3,561,100	$3,595,600	$3,189,500	$3,214,400
Per kwhr used at				
30% annual load factor	6.78 mills	6.27 mills	6.08 mills	5.58 mills
45% annual load factor	4.52 mills	4.18 mills	4.05 mills	3.72 mills
60% annual load factor	3.39 mills	3.13 mills	3.04 mills	2.79 mills
Per route-mile per year	$10,800	$10,900	$9,670	$9,740
Per track-mile per year	4,760	4,810	4,270	4,290
Per thousand gross ton-miles per year	0.587	0.378	0.526	0.338

Table IX. Annual Cost. A-C Electrification

(Cost in dollars, except where indicated)

Item Description	85 Per Cent Lagging Power Factor		95 Per Cent Leading Power Factor	
	100 Per Cent Traffic	157 Per Cent Traffic	100 Per Cent Traffic	157 Per Cent Traffic
Entire System				
Total cost	3,561,100	3,595,600	3,189,500	3,214,400
Cost per kwhr at				
30% annual load factor (mills)	6.78	6.27	6.08	5.58
45% annual load factor (mills)	4.52	4.18	4.05	3.72
60% annual load factor (mills)	3.39	3.13	3.04	2.79
Cost per route-mile per year	10,800	10,900	9,670	9,740
Cost per track-mile per year	4,760	4,810	4,270	4,290
Cost per 1,000 gross ton-miles per year	0.587	0.378	0.526	0.338
Main Substations				
Total cost	1,405,000	1,439,500	1,175,000	1,200,000
Cost per kwhr at				
30% annual load factor (mills)	2.67	2.50	2.24	2.09
45% annual load factor (mills)	1.78	1.67	1.49	1.39
60% annual load factor (mills)	1.34	1.25	1.12	1.04
Cost per kw of installed frequency changers per year	6.53	6.02	5.45	5.02
Cost per 1,000 gross ton-miles per year	0.232	0.152	0.194	0.126
Autotransformer Substations				
Total cost	401,700	401,700	359,900	359,900
Cost per kva of installed autotransformers per year	1.40	1.40	1.50	1.50
Cost per 1,000 gross ton-miles per year	0.066	0.042	0.059	0.038
Feeder-Trolley Circuits—Main Line and Yards				
Total cost	1,754,400	1,754,400	1,654,500	1,654,500
Cost per route-mile per year	5,320	5,320	5,010	5,010
Cost per track-mile per year	2,350	2,350	2,210	2,210
Cost per 1,000 gross ton-miles per year	0.289	0.184	0.273	0.174

Comparison of A-C and D-C Electrification Costs

Comparison of costs of the two systems, to be correct, should be made on the basis of gross ton-miles moved rather than on the basis of cost per kilowatt-hour.

The foregoing figures given reveal that the two systems cost essentially the same.

The total annual costs, exclusive of cost of purchased power and that of the locomotives, for the same conditions per thousand gross ton-miles per year are as follows:

A-c electrification 95 per cent leading power factor $0.338

D-c electrification $0.372

A-c electrification 85 per cent lagging power factor $0.378

The total capital cost of the d-c electrification, at assumed rectifier unit prices is higher than that of a-c electrifications. This is due to the greater cost of feeder and trolley circuits, and supporting structures. On the other hand, the total annual cost of the d-c electrification is lower than that of a-c electrification at 85 per cent lagging but higher than that at 95 per cent leading power factor. This is due to smaller losses in the d-c system.

Summary and Conclusions

1. It is entirely feasible to design an adequate energy conversion and transmission system of reasonable cost for either a-c or d-c electrification of a railroad with moderate traffic density. The heavier the traffic or the grades the more economical electrification becomes.

2. For such a system the annual cost, exclusive of purchased power, is of the same general magnitude as the cost of this power. This should be considered as a very moderate price to pay for the greatly reduced maintenance and improved performance of electric motive power.

3. When the utility undertakes the capital expenditure for the main substations, the initial capital expenditure by the railroad is materially reduced, in return for which the railroad would have to pay a higher rate for electric energy.

4. The economy of railroad electrification depends to a large degree on the ratio of maximum to average traffic density.

5. By means of power interchange between conversion stations or the "peaking"-power transfer scheme in a-c systems, it is possible to reduce the spare frequency-changer capacity to approximately the capacity of one unit on the entire system.

6. In a-c electrification appreciable savings in capital and annual costs can be obtained by approaching a 95 per cent leading power factor at the locomotive.

7. Higher feeder and trolley voltages may produce further savings where the use of such voltages is indicated.

8. Capital and annual costs of d-c electrification will be lower than those submitted if the costs of rectifier units and other equipment decrease with further development. The present indications are that this will be accomplished.

9. Data in this paper are not intended a conclusive comparison of a-c with d-c systems but are presented to assist in estimating the economic results to be obtained by conversion to electric operation on a railroad of moderate traffic density. To obtain a definite comparison, regardless of traffic, it is necessary to consider not only the costs discussed but also operating expenses in connection with other major elements of electrification, such as cost of electric energy, of locomotives and the cost of modification of wayside installations. In any case of contemplated railroad electrification a thorough analysis of the particular problem is required.

10. The information presented is believed to be of sufficient scope and detail so that comparisons with present operating costs can be made, thus assisting railroad management to determine the value of electrification as a solution to their many complex problems.

Table X. Annual Cost. D-C Electrification

(Cost in dollars, except where indicated)

Item Description	100 Per Cent Traffic	157 Per Cent Traffic
Entire System		
Total cost	3,456,100	3,547,300
Cost per kwhr at		
30% annual load factor (mills)	9.02	8.97
45% annual load factor (mills)	6.02	5.98
60% annual load factor (mills)	4.51	4.48
Cost per route-mile per year	10,450	10,750
Cost per track-mile per year	4,620	4,740
Cost per 1,000 gross ton-miles per year	0.369	0.372
Main Substations		
Total cost	1,548,400	1,639,600
Cost per kwhr at		
30% annual load factor (mills)	4.04	4.14
45% annual load factor (mills)	2.69	2.76
60% annual load factor (mills)	2.02	2.07
Cost per kw of installed rectifiers per year	10.62	10.88
Cost per 1,000 gross ton-miles per year	0.255	0.172
Intermediate Tie Substations		
Total cost	72,700	72,700
Cost per 1,000 gross ton-miles per year	0.012	0.0076
Feeder-Trolley Circuits—Main Line and Yards		
Total cost	1,835,000	1,835,000
Cost per route-mile per year	5,550	5,550
Cost per track-mile per year	2,450	2,450
Cost per 1,000 gross ton-miles per year	0.303	0.193

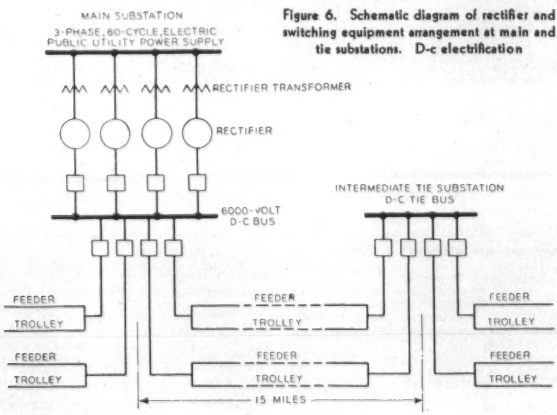


Figure 6. Schematic diagram of rectifier and switching equipment arrangement at main and tie substations. D-c electrification

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Discussion

T. M. C. Martin (Bonneville Power Administration, Portland, Ore.): Whatever the intentions of Kimball and Holm in attacking the problem of electrifying the 330-mile section of railroad assumed in their paper the result is to establish beyond reasonable doubt the uneconomic nature of the solution offered. The average daily traffic, both passenger and freight, assumed by Kimball and Holm is only about 50,000 tons per day (for their 100 per cent traffic case) over the section including movements in both directions. This is to say that the railroad is one of "moderate traffic density." In the circumstances it can scarcely be presumed that such a railroad could indulge in the character of operations assumed by Kimball and Holm—operations which automatically inflate the capital costs of electrification by not less than 100 per cent. The assumed railroad is not a heavy grade operation, having a ruling gradient of a little less than 1.0 per cent. With such sparse traffic and such a relatively easy profile it is extremely disturbing to find the authors imposing schedule requirements that assure the worst possible load factors and consequently the least likelihood of achieving reasonable power costs. What is equally disturbing is the wide-open case made by such a presentation for the installation of Diesel-electric locomotives. The authors have implied a cost in round numbers of about $24,000,000 for this 330-mile section of electrification, entirely exclusive of motive power. The writer will hazard the guess that because the assumed method of operation involves very poor utilization, an additional investment of not less than $16,000,000 for motive power would be required, making a total investment of around $40,000,000 or about $120,000 per route mile.

The writer feels very confident that the application engineers of any of the Diesel manufacturers would find such a situation made to order. It seems certain that they would insist upon enough better utilization of motive power to enable them to dieselize this same section for a total expenditure of about $10,000,000, which amount would largely be expended for motive power.

The writer does not wish to leave the impression that he believes dieselization of such a section either desirable or inevitable. Quite the contrary he has been privileged to participate in a recent study looking toward the electrification of 350 miles of western main-line crossing the Cascade Mountains against ruling gradients of 2.2 per cent (more than twice as steep as those assumed by Kimball and Holm) and as a result thereof it is his firm conviction that such an electrification can be accomplished even under present prices with ample reserve capacities in both trackside power supply apparatus and motive power for about the following sums:

Power supply including trolley, transmission, trackside substation apparatus, and so forth	$12,000,000
Electric motive power	8,000,000
Total electrification capital cost	$20,000,000

This is less money for everything including motive power to operate against twice as rugged a profile, and it is based on actual operating practices of a leading western railroad. Finally from a power supplier's standpoint it is based upon realistic appreciation of the necessity of better electrical load factors as a prerequisite to more favorable operating costs so far as cost of electric power is concerned.

When appraising electrification possibilities it seems unwise for engineers to handicap electrification chances by assuming that it is necessary to provide trackside power supply apparatus capable of withstanding peak demands of twice the magnitude that could possibly be imposed by the simultaneous operation at full load of all the motive power units which normal efficient utilization would dictate as being the maximum number the railroad should purchase. For the purely main-line operations of the section assumed by Kimball and Holm it can be shown that about 60,000 continuous rail horsepower of locomotives would be entirely adequate to handle their 100 per cent traffic case. Were all locomotives taking full power the simultaneous substation demands would hardly aggregate more than 70,000 kilowatts. When it is remembered that to protect this possibility Kimball and Holm have suggested 240,000 kva of frequency changer capacity and correspondingly large aggregates of trackside substation capacity it will be realized that their conservatism has led them into a very considerable overinstallation of roadside equipment.

H. F. Brown (New York, New Haven and Hartford Railroad, New Haven, Conn.): The title of the paper by Kimball and Holm while justified by existing power generation conditions today indicates why the Diesel-electric locomotive enjoys its present economic advantage. I refer to the phrase "Energy conversion costs."

The Diesel-electric locomotive generates without further conversion the type of electric power required by the locomotive; at present almost exclusively direct-current but alternating-current with its additional advantages is already being carefully considered.

Why must the railroads purchase electric power for traction purposes in a form they cannot use, and then with its own investment, and operators, and maintainers, convert this power into a form they need? Naturally, the railroads would prefer not to be in the "power business," if they can purchase at reasonable cost the power they need; but if they have to do this with additional investment, mentioned in this paper as 40 per cent of the total investment exclusive of locomotives, and then have additional operating and maintenance expense, they might better go all the way and own and operate their own power station, because they are in the "power business," even if it is only for "conversion" and operation of conversion facilities.

The problems of owning and operating power plants are of no more magnitude than owning and operating converting stations, and the economies are very real. A pioneer single-phase eastern electrified railroad still operates its own 25-cycle power plant, which supplies about 55 per cent of its total requirements. It purchases about 40 per cent of 25-cycle single-phase power directly as such from a large utility which generates both 25 and 60 cycles with both systems tied together. It purchases about 5 per cent of its power at 60-cycles from another utility, paying all operating and maintenance costs in addition to power rates approximately equal to the equivalent 25-cycle block purchased directly from the other utility, and absorbs in so doing an average 18 per cent conversion loss. The "cost of conversion" indicates why a greater use is not made of the 60-cycle supply.

The power companies could develop a large and profitable base load, in time, if they would give more attention to this problem—a potential load which often parallels their own power networks. One or two large utilities generate both 25 and 60 cycles. The largest commercial hydro plant in Canada—the Beauharnois plant—has 5 of its 12 wheels generating 25 cycles, all feeding into Ontario.

The power utilities can be complacent and say "60-cycle power is standard for all uses, and if some other frequency is needed the customer will have to change it"; or they can be complaisant, and do much to encourage railroad electrification, as part of the general electrification of all industry, by generating the kind of power necessary for this potential business, which, all factors considered, could be 25 cycles regardless of the form of electrification used.

Just as power systems can profit by interconnection, so can railroads, with their own station or stations, interconnected with other power sources, profit by so doing, in the interchange of power during periods of wide diversity. This may involve additional conversion equipment, the cost of which could be shared, but of far less capacity than involved in the major supply.

Many of the earlier eastern electrifications, although some have leased their owned electric plants to the large power utilities in their area, are still profiting from having owned their own stations in their present power rates.

Power conversion costs, as such, should not be so great a charge against railroad electrification.

Kimball and Holm mention the economies of the 95 per cent leading power factor locomotives, without being specific as to just how this is accomplished. It is obvious that if the means used makes the motive power more expensive, that difference in cost will have to be subtracted from the economies realized in the transmission.

The choice of voltage, both alternating and direct current, tend to make this paper seem just a bit impractical. Surely, if 6-kv can be justified for the d-c electrification, 12-kv can be justified for the a-c study, and with the experience over the past four decades by the railroads with the largest amount of electrified mileage, and in the interests of standardization, the justification for the choice of these voltages does not seem to be clearly defined. Possibly a factor could be supplied in each case to show the saving accomplished by going to these higher voltages.

The paper specifically excludes locomotives and no estimate as to the number required is given, other than to state that they are used interchangeably in both passenger and freight service. Nevertheless, in the over-all economic picture they must be considered.

If it is assumed that between 20 and 35 will be required to take care of the assumed

traffic, and if it is assumed that electric locomotives will cost $300,000 each, a total investment, (in addition to that for the conversion and transmission facilities of between $23,000,000 and $29,000,000) of $6,000,000 to $10,000,000 will be required for locomotives. If it now be assumed that Diesel-electric locomotives could handle the service equally well, and that, for the same rail horsepower such locomotives will cost $500,000 each, the railroad management would have to decide whether to purchase between $10,000,000 and $17,500,000 worth of Diesel-electric locomotives, presumably with equipment-trust certificates, or to purchase, in the same manner, between $6,000,000 and $10,500,000 worth of electric locomotives, and then find some way of financing an additional investment of $23,000,000 to $29,000,000 in fixed property, a somewhat difficult task in these days for even the most prosperous railroads.

The difference in total investment between electrification and Dieselization is between $19,000,000 and $22,000,000. Even if it could be shown that electric operation could effect savings over Diesel operation to warrant this investment, the matter of financing would still be the deciding factor.

Sidney Withington (New York, New Haven and Hartford Railroad, New Haven, Conn.): Kimball and Holm mention the cost of purchased power as varying "from four to six mills per kilowatthour at 60 cycles." It would be interesting to know of any utility which would deliver power at such rates even under the most favorable load factor specified and even at 1939 costs. Evans sets up perhaps more realistic costs running from "0.5 cent to 2 cents per kilowatthour," depending on load factor and other considerations. Evans' power analysis gives hydro power system demand costs of 90 cents per kw per month, presumably integrated hour. This would inferentially represent power solely from a system which does not require any steam plant stand-by for use in dry periods or under flood conditions—a situation which does not occur in most sections of this country. It would be of interest to have more information on this.

It is indicated that the hypothetical railroad described has 90 miles of two single-track rights-of-way. The diagrams indicate that these two routes are supplied from the same d-c substation and that trolleys and feeders are tied together at 15-mile intervals at the substation, and that the a-c network ties them together at two intermediate points. A word as to the separation of these two rights-of-way would be of interest. Presumably there would be a 48-kv a-c feeder along each of these single track routes. A word on this would be interesting.

The demands per train at a maximum of 7,500 kw (integrated hour) indicate heavy motive power. The assumed figure of 37 kilowatthours per 1,000 gross-ton-miles (with locomotive 41 kilowatthours) seems somewhat low when compared with figures reported by existing railroads of this general class of service. It is presumed this is measured at the point of supply rather than at the shoes, but a word on this would be interesting.

In this connection it is not made clear why the demand per train for a-c varies between 5,000 and 7,500 kw and for d-c 5,000 and 7,040 kw, while the energy is greater for the direct-current system, all for the same train service.

It appears that for the single-phase supply, twelve frequency changers rated at 20,000 kw are contemplated at four substations, or 60,000 kw per substation. Evans states that "combining substations has no merit," though such combination he points out would save $229,000 per year in demand charges. Evans indicates that costs of transmission tying systems together would amount to $2,400,000 in addition to substation facilities at $20 per kilowatt. In view of the fact that the catenary supporting structures are available for high voltage circuits, a cost of $20,000 per mile for the 100-kv circuits seems generous, even at 1939 prices. Furthermore, with 48–24-kv single-phase tie circuits, considerable "combining of loads" would be practicable with the system as designed. Furthermore, it may be presumed these systems are tied in on the 60-cycle side by means of utility networks, and this would permit combining load through co-operation among the utilities. The power saving through combining loads in order that the railroad would not have to pay for the same demand twice might well affect the decision of the railroad whether or not to adopt electrification. Kimball and Holm indicate 8 per cent is sufficient estimate for railroad capital requirements, while Evans figures 10 per cent. The railroad would presumably be consistent in taking a uniform figure for all such capital expenditures

Evans suggests the railroad can save on its power bills by creating a diversity of its own by "providing for trains standing in sidings—and traveling partly loaded." This may be practicable on some railroads, but most roads must operate service when it is offered and as expeditiously as possible, and must handle trains of maximum practicable tonnage at all times. The power supply must be employed to operate the railroad most efficiently, and operation of the railroad to benefit the supply by careful dispatching of trains, as Evans suggests, is not popular with most railroad managements.

Kimball and Holm indicate that the utilities supplying power might be willing to install the frequency-changer or rectifier equipment. This would be a consummation devoutly to be wished from the railroad viewpoint, because most railroad organizations are not equipped for operating and maintaining substations of these kinds. It would be very doubtful, however, if, in figuring fixed charges for capital, any power company would be satisfied with return of 8 per cent, as indicated, to cover interest, depreciation, insurance, and taxes, and there would be, as implied, very considerable "increased cost of purchased power," which might change the analysis materially.

Kimball and Holm indicate that as between alternating and direct current the capital costs "are essentially the same," and they tabulate the data to show that a-c electrification capital costs are $2.45 and d-c electrification $3.12 per thousand gross ton miles, and that annual costs (exclusive of purchased power and locomotives) are $0.338 for alternating current and $0.372 for direct current (95 per cent leading power factor a-c electrification in each case). Evans shows that power bills for 157 per cent traffic with no diversity are 0.997¢ per kilowatthour for alternating current and 1.101¢ for direct current per kilowatthour. The advantage in this analysis would appear to rest with single phase.

Comparisons between systems of electrification are open to a vast amount of discussion, and any engineer who estimates relative costs and advantages is courageous indeed. Evans and Kimball and Holm are to be congratulated on their courage and their stimulating analysis of this complicated question.

K. H. Gordon and **L. B. Curtis** (Pennsylvania Railroad, Philadelphia, Pa.): The excellent paper prepared by Kimball and Holm is of current interest and should stimulate increased discussion of further electrification possibilities.

However, it is our opinion that the statement "The information presented herein is believed to be sufficient for making comparisons with present railroad operating costs and for assisting railroad management in estimating the value of electrification as a solution to the many complex problems that are demanding attention today," is unduly optimistic and gives questionable assurance that comparisons of sufficient and necessary accuracy can be made. It overlooks these factors:

1. Over-all cost comparisons between steam, electric, and Diesel operation show results too close and reveal a number of debatable factors that can swing the answer one way or another.

2. Present a-c and d-c systems, after years of operation and experience, still have problems showing up that increase the over-all cost of electrification and its operation.

3. The paper under discussion develops a comparison of costs based upon two hypothetical untried systems. The higher a-c and d-c voltages used and distribution layouts assumed based upon these voltages give rise to the following problems, the cost of which might make the proposed electrification unattractive:

a. The 30-mile spacing of autotransformer substations and the 15-mile spacing of d-c tie stations offer selective relaying problems with only theoretical solutions now available.

b. The 30-mile and perhaps 15-mile sections proposed, when out of service, will tie up a large section of railroad and may stop two or more trains for lack of power.

c. The 24,000-volt contact system will offer serious clearance problems in restricted overhead areas such as tunnels, overhead bridges, stations, and so forth. This may require expensive track lowering or other costly solutions.

d. Mercury-arc rectifiers for 6,000-volt traction service are not available now, and may not be available for years to come. Before they could be placed in service in the future, a considerable amount of development and experimentation will be required.

e. The loss of a d-c substation would result in a section of 120 miles between supply points, which even with all tie circuit breakers in service, will give seriously low voltage at the midpoint.

f. The assumption that for the d-c system, "phase shifters for basic wave-shape correction will presumably meet all except the most unusual requirements of inductive co-ordination" is questionable and unproved. It is more likely with the railroad type of rapidly fluctuating and moving loads that further expensive measures of mitigation will be required.

g. New locomotives required for the higher voltages would be more costly as they would be a new development.

h. Extensions to existing railroad electrifications at the higher voltages would create an expensive problem of adapting present motive power to operate from two or more voltages.

i. Proposed voltage systems would throw farther into the future the standardization, and hence the lowering of prices, of electric locomotives and apparatus for substations.

In view of the foregoing, conclusion No. 7 on page 8 of the author's paper that "Higher feeder and trolley voltages may produce further savings where the use of such voltages is indicated" is highly questionable.

D. R. MacLeod (General Electric Company, Erie, Pa.): In their paper on energy conversion and transmission costs, Kimball and Holm have included a 6-kv d-c system. Higher voltage d-c systems have been proposed ever since the first 3,000-volt d-c system was put in successful operation. Studies have been made of systems using voltages as high as 6-kv directly applied to the traction motors. The technical difficulties that have to be overcome are formidable. Every estimate has indicated that the increased cost of the rolling stock would more than offset any saving in energy conversion and transmission costs. Similar studies have been made of locomotives using rectifiers to transform the trolley voltage to a lower voltage for application to the traction motors. The results of these studies have likewise been discouraging. The results obtained by Kimball and Holm are further confirmation that there is no advantage in the 6-kv d-c system over other systems now in operation.

The 24-kv 25-cycle system used in this study would be capable of handling far more ton-miles than are given in the paper. The fleet concentration that is shown in Evans' paper[1] on power costs and described in Kimball and Holm's paper as the basis for designing the power supply, is, in our opinion very unrealistic. It is doubtful if the yard facilities that are available on a railroad operating a total of 13 trains per day would permit dispatching freight trains at frequent enough intervals to bunch the traffic in the manner indicated. A much better load factor would also have been obtained if a more realistic distribution of trains had been used as a basis for designing the system. The results of the study as shown in Tables II and V are apt to be misleading if they are used to compare the cost of electrifying the 330-mile route with the cost of moving the same traffic with Diesel-electric locomotives.

REFERENCE

1. POWER COSTS AND THE CONTRIBUTION OF EXISTING TRANSMISSION NETWORKS TOWARD COST REDUCTION OF POWER FOR ELECTRIFICATION OF MAIN LINE RAILROADS WITH MODERATE TRAFFIC, Llewellyn Evans. AIEE TRANSACTIONS, volume 67, 1948, pages 596–602.

J. G. Holm: The purpose of the paper was to stimulate interest in the subject and to invite discussion. No attempt was made to prove the advantages of one particular system of electrification over another. However, attention was directed to certain features of electrification, particularly the advantage of a leading power factor in the locomotive. Whenever these features were considered, they were always co-ordinated so as to result in a most favorable over-all electrification. It was hoped that this aspect of the paper would not be overlooked, and so it is gratifying to see its recognition in most of the discussion.

Since the discussions presented do not lend themselves to a common reply, they must be handled separately.

Withington very appropriately points out the advantages of interconnections between the public utilities supplying power to the railroad. His statement that "Most roads must operate service when it is offered" is of prime significance and implies that the roads must furnish service under all adverse conditions, in particular those arising from unexpected "bunching" of trains.

Withington points out that the cost of purchased 60-cycle power mentioned in the paper at from 4 to 6 mills per kilowatthour is too low, and that Evans in his paper[1] considered "more realistic" costs. Attention is called to Table II, Station C, of Evans' paper, where the cost of power is given at 4.37 mills per kilowatthour. The fixed charges of 8 per cent apply only to railroad financing, and public utility fixed charges for their part of the installation may be in the order of 10 per cent. The demand per train is measured at the point of supply and the assumed figure of 41 kilowatthours per thousand gross ton-miles (with locomotive) compares with 45 kilowatthours which is an average for four railroads. The two single-track rights-of-way are assumed close enough together to be fed from a common substation. The demand per train on the a-c system is greater than on the d-c system because of the losses in frequency changers, in transformers, and the reactive losses in the trolley-feeder circuits.

Brown goes even further than Withington in discussing the benefits from interchange of power during the periods of diversity. With his suggestion that the utilities generate the kind of power necessary for railroad business we are in complete agreement. The leading power factor in a locomotive may be obtained either with motor generators operating leading or with series motors whose power factor is sufficiently corrected. To accomplish this, some capacitors would have to be installed in the locomotive. Brown is correct in stating that their cost must be included in the estimates.

Brown, Gordon, and Curtis question the selection of a-c and d-c voltages. It is stated in the paper that the hypothetical railroad has no clearance problems, which is true of numerous railroads. In view of this, an a-c voltage of 24-kv with its advantages over 12-kv was used, and to give the d-c system comparable advantages, 6-kv direct current was selected.

Gordon and Curtis state a number of separate problems which in their opinion disqualify the use of higher voltages and distribution layouts assumed. The electrification discussed in the paper was assumed to be an initial electrification project and not an extension to an already partially electrified railroad. The problem of adapting existing motive power to a 2-voltage system is, therefore, eliminated. The locomotive standardization will be only slightly affected by the higher voltage suggested. With automatic reclosing of circuit breakers, normal outages will be reduced, so that interference with train movements will not be appreciable. All systems have sufficient copper in the trolley-feeder circuits to insure required midpoint voltages in case of loss of a substation so that train operation can be maintained under most adverse conditions. The problem of selective relaying has never been permitted to limit the fundamental design of an electrification. Carrier-current relaying is a possible solution. In so far as inductive co-ordination is concerned, it should be noted that in the proposed use of 12-phase rectifiers the problem of decreasing the harmonic voltages is very much easier than with 6-phase rectifiers from which perhaps the experience of the discussers is drawn.

Gordon and Curtis point out that a considerable amount of development will be required before the 6-kv rectifier will be on the market. Assurances have been received that when a demand for such rectifiers is evident, they will be supplied. In the paper ample provision for the development costs of 6-kv rectifiers has been made.

MacLeod mentions "formidable" difficulties to be overcome in the development of the 6-kv rectifier, so that the cost will more than cancel savings in the power supply system. MacLeod's opinion is that of one person in one company and his statement, to be accepted, must be supported by figures. It is hard to believe that the difficulties are so "formidable" that they will not be overcome without upsetting the over-all economic situation which definitely points to advantages of higher d-c voltages. On the other hand, there is no fundamental reason why two 3-kv rectifiers could not be connected in series, to give the desired 6,000 volts.

MacLeod and Martin criticize the train fleet concentration, calling it "unrealistic" and "disturbing." To assume that the "bunching" of trains was considered a normal operation is wrong. However, when normal train dispatching is assumed, provision must be made for emergency operation, during which the "bunching" of trains occurs due to unpredictable factors. Agreement with the degree of "bunching" assumed is not necessary, although it was based on the experience of eastern railroads, but not to allow for any "bunching" is to ignore the exigencies of railroad operation. Even though this may be condoned by some western road, it is not generally accepted practice. It is significant that none of the railroad operating men commenting on the paper objected to the assumed "bunching" of trains. Similarly, dispatching trains to suit the power load demand is not generally practiced and "not popular with most railroad managements," quoting from Withington's discussion.

MacLeod and Martin say that the road can handle a traffic larger than given in the paper. No statement to the contrary was made. Traffic designated as 157 per cent does not signify that the system is overloaded by 57 per cent. The two levels of traffic called 100 and 157 per cent might just as well have been called 63.5 and 100 per cent. Martin based all of his statements on the smaller traffic and simply ignored the larger traffic, despite the statement that at a traffic even greater than the 157 per cent figure the costs will become smaller.

In view of the foregoing, Martin's statement that for the 100 per cent traffic a total substation capacity slightly over 25 per cent of that described will suffice, cannot be taken seriously. With normal dispatching and accidental "bunching" of freight trains as assumed in the paper for the two traffics, the train sheet shows that the peak demand at each main substation is about 50,000 kw for 100 per cent traffic and 75,000 kw for 157

per cent traffic. This compares with the 60,000 kw which was assumed installed at each substation for the 157 per cent traffic. During the most adverse conditions of "bunching," the load factor is about 57 per cent. Allowing for some "bunching," the average load factor is somewhere between this figure and the 22 per cent obtained with perfect train operation after normal dispatching. An analysis of Martin's figure of $12,000,000, at current prices, indicates that his costs, reduced to a unit basis, are about 75 per cent of the corresponding 1939 costs used in the paper. Just as Martin is firmly convinced that he can electrify the assumed railroad for $12,000,000, so are we certain that his estimate is substantially inadequate. In his paper of 1946[2] Martin erred still more on the side of underestimating costs by giving a $10,000,000 figure for the electrification of a 400-mile railroad.

It is impossible to check the stated value of motive power Martin has determined to be necessary for 100 per cent traffic. It is to be understood that the authors guarded against presenting figures that are too "conservative" and tend "to handicap electrification chances." The intent of the paper was to give figures of electrification costs that are generally realistic rather than wishfully low.

Several speakers discussed dieselization. The paper made no attempt to compare electrification with dieselization. If railroad management would listen to the Diesel application engineer's "insistence" on better utilization of motive power, the same management would also listen to the many savings possible with electrification. These savings were only slightly considered in the paper. It may be stated that while today it is easier to acquire a Diesel locomotive than to furnish the capital required for electrification at present costs, possibly in the near future money, fuel and market conditions may change. With the cheaper operation of electric locomotives, this may place all the advantages on the side of electrification.

In conclusion, we wish to thank the discussers for their contributions.

References

1. Power Costs and the Contribution of Existing Transmission Networks Toward Cost Reduction of Power for Electrification of Main Line Railroads with Moderate Traffic, Llewellyn Evans. AIEE Transactions, volume 67, 1948, pages 596–602.

2. Comparisons of Railway Motive Power for Operation in the Pacific Northwest, T. M. C. Martin. AIEE Transactions, volume 65, 1946, pages 1054–65.

Comparative Operating Results of Steam, Diesel-Electric, and Electric Motive Power on the Great Northern Railway Electrification

JOSEPH F. N. GAYNOR
NONMEMBER AIEE

Synopsis: The 75-mile single-phase 11-kv electrification of the Cascade Mountain division of the Great Northern is described briefly and the comparative performance of steam and electric locomotives is given, indicating the ability of the electric locomotive to handle 6 times the number of ton miles per hour formerly handled by steam locomotives. Mixed operation, using steam, Diesel-electric, and electric locomotives in the same trains, during the war, is described, and comparative costs of operation and maintenance for each type of motive power are given. The superior advantages of the motor-generator type of electric locomotive are set forth. Further extensions of this electrification are shown to depend on cheaper electric power costs, without restrictive demand rates. The future of electrification, in general, is shown to be closely associated with standardization of system by manufacturers, and the possible coordination of motor-generator type locomotive apparatus and equipment with that of other generator type motive power.

THE CASCADE MOUNTAIN DIVISION of the Great Northern Railway originally crossed the mountains by switchback. Later, a 3-mile tunnel was driven through the mountains between Tye, Wash., on the west side, ascending eastbound on a 1.7 per cent grade to Old Cascade on the east side to the 4,100 foot summit. Operation was by steam locomotives until 1909, when the tunnel was electrified, with 3-phase 25-cycle 6,600-volt alternating current on the double overhead contact system. The 3-phase system was selected on this pioneer electrification to obtain the required high torque characteristics of the induction motors used in the locomotives. By operating two units in multiple, with motors connected in series or concatenation, the heaviest freight trains of that period, together with their steam engines, were hauled through the tunnel at $7^1/_2$ miles per hour. This original tunnel electrification operated successfully from 1909 to 1927.[1]

The success of this 3-mile tunnel electrification turned the attention of the railroad to the benefits of electrifying its entire mountain district, between Skykomish on the west side at the foot of the 2.2 per cent grade, to Wenatchee on the east side, for a distance of 73 miles, via the new 8-mile Cascade Tunnel between Scenic on the west, and Berne, the new summit at 2,800 foot elevation on the east side.

The single-phase 25-cycle 11-kv system was selected for this new electrification, and while the new tunnel construction was in progress, the old line from Skykomish to Tye through the original 3-mile tunnel to Old Cascade, about 25 miles, was equipped for single-phase operation at 11 kv. Motor-generator type electric locomotives were selected for the single-phase electrification because of their desirable characteristics of greater capacity for handling at higher speeds any tonnage decided upon. The 25-mile section over the old line was abandoned when service was inaugurated over the 73-mile new line through the 8-mile tunnel in January 1929.[2]

During the operation of the old line with single-phase motor-generator locomotives, the advantages of electrification immediately became apparent. Steam hauled freight trains of 2,500 tons required five hours to cover the 25-mile section on 2.2 per cent grades. Three Mallet type locomotives per train were used; water stops had to be made on the ascending grade, while on the descending grades, stops had to be made to cool wheels and to inspect train for dragging brake beams, cracked wheels and so forth. Electric operation on the same section was with 3,000 ton trains, 2 electric locomotives, and running time of $1^1/_2$ hours on the ascending grades. The run on the descending grades, in regeneration, was nonstop in $1^1/_4$ hours.

The comparative performance of steam and electric motive power for this mountain service, in train ton miles per locomotive hour was as follows:

$$\text{Steam} = \frac{2{,}500 \text{ tons} \times 25 \text{ miles}}{3 \text{ locomotives} \times 5 \text{ hours}} = 4{,}150 \text{ ton miles per locomotive hour}$$

$$\text{Electric} = \frac{3{,}000 \text{ tons} \times 25 \text{ miles}}{2 \text{ electric locomotives} \times 1^1/_2 \text{ hours}} = 25{,}000 \text{ ton miles per locomotive hour}$$

giving a performance ratio between electric and steam of 25,000/4,150 or 6 to 1 in favor of electric operation on motive power performance alone. As to crew cost, electric trains had but one helper crew and one pilot conductor, while steam trains required 2 helper crews and 2 pilot conductors.

The running time for steam trains between Skykomish and Wenatchee required never less than 15 to 16 hours for this 73-mile run. This entailed not only a large amount of crew overtime, but extra crew cost as well, for relieving crews on the road in compliance with the 16 hour Federal service law. The electric trains covered this distance in 4 to 5 hours, maximum, making a very substantial saving in crew cost. After electrification, train tonnages were increased in

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JOSEPH F. N. GAYNOR is superintendent, electric operations, Great Northern Railway, Wenatchee, Wash.

successive steps up to 5,250 tons with no loss in running time. This was accomplished by multiple unit operation of 2 and 3 electric cabs so that one road crew and one helper crew could handle the maximum tonnage. It was demonstrated by numerous road tests on the 2.2 per cent grade that the motor generator type locomotives could easily handle 6,000 ton trains, this limit being determined by passing siding capacity rather than locomotive tractive effort. But for this limitation, a maximum tonnage of 7,500 could be handled.

The proper location of helper locomotives is an important factor in operating such heavy tonnage trains on grades as heavy as 2.2 per cent. Experience on this electrification indicates that 3,000 tons, trailing, behind the "swing" or middle helper, is about the maximum allowable, the balance of the tractive effort of these locomotive units being utilized to push up to 1,500 tons ahead. Head end power concentrations up to 10,000 horsepower are used successfully in such service. Maximum total horsepower per train is from 17,000 to 20,000 horsepower at rail, with continuous tractive efforts of 315,000 to 408,000 pounds, average, and up to 471,000 pounds when required for maximum tonnage. With the swing helper system, the limitations in tonnage are the length of passing tracks, and the capacity of the electric power supply, rather than the strength of drawbars. The trailing continuous tractive effort is limited to about 225,000 pounds, maximum.

The motor-generator type is the only locomotive having the necessary characteristics to move such tonnage up hill, and to hold the same by regeneration on down grade at any speed from a slow walk to the maximum capacity speed of the locomotive. Electrification alone made this type of operation and performance possible; and 20 years of satisfactory operation has proved its reliability.

The profile of the Great Northern Electrification District starting eastward at Skykomish, Wash., ascends for 12.8 miles on a compensated 2.2 per cent grade to the west portal of the 8-mile tunnel at Scenic. The tunnel grade is 1.56 per cent for 8 miles ascending to Berne, the east portal and also the summit. From Berne to Merritt is a 2.2 per cent descending grade for 7 miles, then 7 miles of rolling profile of 0.3 per cent to Winton, where the new Chumstick Cutoff line descends on a 1.6 per cent grade to Peshastin for 16.5 miles. From Peshastin to Cashmere is 8 miles of 1 per cent grade descending, changing to 0.5 per cent descending for 11 miles to Wenatchee, Wash. The eastern terminal and electric shops are located at Appleyard, 2 miles east of Wenatchee. The maximum curvature is 10 degrees. The average running time of electric freight trains over the electrified district is $3^1/_2$ hours, including one inspection stop to look over the train.

Table I. Great Northern Railway Electrification Motor-Generator Locomotive Characteristics

	Westinghouse Electric and Manufacturing Company	General Electric Company	General Electric Company
Great Northern Classification	*Z 1*	*Y 1*	*W 1*
Purchased new	1926	1927	1947
Total weight per cab or unit—pounds	360,000	518,250	720,000
Weight on drivers—inches	275,000	414,000	720,000
Wheel arrangement	1-D-1	1-C + C-1	B-D + D-B
Diameter drivers—inches	56	55	42
Diameter truck wheels—inches	36	36	42
Gear ratio	18/91	21/82	17–70
Length over cab	43 ft. 0 in.	63 ft. 0 in.	97 ft. 8 in.
Length between couplers	47 ft. 0 in.	74 ft. 0 in.	101 ft. 0 in.
Rigid wheel base	16 ft. 9 in.	15 ft. 4 in.	16 ft. 9 in.
Total wheel base	31 ft. 5 in.	58 ft. 8 in.	85 ft. 9 in.
Multiple-unit operation—number of units	2 and 3	1, 2, 3	1, 2
Trolley voltage—kv	11	11	11
Synchronized motor voltage	1,250	2,500	1,350
D-c main generator voltage	600	750	450/700
Motor-generator Set rpm	750	750	750
Tractive Effort lbs continuous	45,000	61,000	122,000
Tractive Effort lbs hourly	57,000	67,200	136,000
Tractive Effort lbs starting at 25 per cent adhesion*	68,000	103,000	180,000
Continuous speed—miles per hour	15.5	18.6	15.75
Maximum speed—miles per hour	45	55	67.5
Continuous rail horsepower	1,860	3,000	5,150
Continuous tonnage per unit on 2.2 per cent grade	750	1,000	2,000
Number of single units	10	8	2

* Electrical capacity good up to 50 per cent adhesion.

The reliability of the 11-kv inclined catenary contact system and the twin circuit 44-kv single-phase transmission lines on butt-treated cedar poles has been close to 100 per cent. Although slides and forest fires have caused delays, the service has never been interrupted by electric power failures.

The electric motive power of the Great Northern consists of 20 motor-generator type locomotives. Their classification and characteristics are shown in Table I. From this table it will be seen that the trend is toward higher powered single unit electric locomotives, capable of developing the full rated rail horsepower over the entire speed range. The Great Northern class *W-1* is the largest single cab locomotive in the world at the present time.

Mountain railroading demands large concentrations of rail horsepower and tractive effort. The basis of all performance is running adhesion continuously obtainable regardless of type of motive power, especially in bad weather. Road tests made during the past 20 years on the heavy grades and curves of the Great Northern electrification have definitely established that the best average running adhesion obtainable on good dry rails is 18 to 20 per cent, regardless of type of motive power. The all-weather average is 15 per cent. Starting adhesion with sand is between 25 and 40 per cent.

Locomotives having permanently parallel-connected electric traction motors give the best performance from an adhesion standpoint, since their driving wheels tend to creep rather than spin when adhesion is lost. Those having series-parallel connected motors have a tendency to spin down to a progressive stall under bad rail conditions, unless a light application of driver brakes is made to arrest spinning drivers and equalize the torque on all axles.

Just prior to the war, in anticipation of an electric motive power shortage, the adhesion characteristics of all three types of motive power—electric, steam Mallet type *R 1*, and 5,400 horsepower Diesel-electric were carefully determined by tests for mixed power operation. During the war, all three types were successfully used in the same train, as routine operation. Mallet pushers, cut in ahead of the caboose, handled 1,500 tons at 15 miles per hour up 2.2 per cent grades; the 5,400 horsepower 4-unit Diesel-electrics cut into the train near the middle handled 2,000 tons; while 2 or 3 units of electric locomotives on the head end handled 2,000 to 3,000 tons. Thus, trains of any desired tonnage, up to the limits set by the length of passing sidings, were successfully handled. The Diesels were operated through the 8-mile tunnel as helper engines, handling their full rated tonnage,

with electric engines on the head end.

In making the tests to determine the hauling capacity of the steam and Diesel-electric locomotives, the motor generator electric locomotives were used, regenerating, as a dynamometer—a role for which this type of locomotive is ideally adapted.

Having established the tractive effort, rail horsepower ratings, and tonnage at various speeds on the 2.2 per cent grade for each type and any combination of types of motive power, the train dispatcher could figure accurately the progress trains would make over the mountain division. The rail-horsepower found for the various types of power are shown in Table II.

Interesting figures on the gross ton miles per unit of fuel, and gross ton miles per dollar were obtained on the 2.2 per cent grade for all three types of power. Gross ton miles include the locomotives shown in Table III.

As steam helper engines were used only on the 2.2 per cent grades, and Diesel helpers were used through the entire electrified zone, the foregoing comparisons were obtained on the 2.2 per cent grade sections where all three types were working together in the same train.

The comparative operation and maintenance cost of the steam, Diesel-electric, and electric locomotives, based on figures obtained during 1941 to 1946 inclusive, in cents per mile are shown in Table IV.

The lubrication cost on the new 5,000 horsepower electrics (*W 1*) equipped with roller bearings throughout, is about 32 cents per 1,000 miles. From the foregoing costs, the electric operation speaks for itself, regardless of high power rates and heavy repairs after 15 to 20 years of continuous service.

From these figures the question of further electrification is seen to be a matter of first investment and power cost. Although the electric locomotive leads the field in performance, relatively low maintenance cost over long periods of time, and has the greatest possibilities of high rail horsepower per unit of weight and size, its first cost is relatively high when those of its contact and power transmission systems are added. Even though the efficiency, flexibility, availability, and utilization possibilities, along with the moderate maintenance cost all tend to offset the high investment cost, over a period of years, the high cost of electric power based on restrictive demand rates and ridiculous time periods in figuring peak demands is a continuing adverse factor, and causes a reduction in utilization, to control the peak demands.

Obviously, the advent of the Diesel, the fuel costs of which are not affected by such artificial and adverse economic restrictions, has changed the picture. They do a good job of railroading, and still can take a large increase in fuel cost before they are reduced to the same gross ton miles per dollar as the electric, based on the 8.5 to 10 mill power rate prevailing on this electrification since 1927.

The effect of electric power cost on future electrification extensions of the Great Northern Railway electrification has been thoroughly studied by the electrical operations and engineering departments of the ralroad, as well as other interested parties. Three proposed extensions have been worked out:

(a). 25 miles west from Skykomish to Gold Bar, at the foot of grades.

(b). 85 miles west from Skykomish to Seattle.

(c). 179 miles east from Wenatchee to Spokane.

A study of the return on investment in each case showed up very favorable, provided electric power could be purchased at 4 or 5 mills per kilowatthour with no restrictive demand charge. With the present power rates, the only extension likely, if any, would be (*a*), the 25 mile section of 1 per cent grade between Skykomish and Gold Bar.

A few years back, when small power companies with limited generating capacity were forced to write restrictive demand charges into contracts to protect themselves and their other customers from large customers taking more than their share of demand, such demand rates may have been necessary. Today, some railway electrical engineers do not agree that these out-of-date, restrictive demand rates and their methods of clocking should apply to modern operations in the midst of the North West hydroelectric power belt, with the Columbia River power dams on the east side of the Cascades furnishing their mighty loads, and the big dams on the west side, all tied into a huge network. So long as those restrictive demand rates apply, electrifications will be few and far between. Meanwhile, competitive forms of motive power will turn revenue to the coal and oil companies that otherwise could and should go for electric power if the rates were more equitable.

In a recent report of the Bonneville Power Administration, T. M. C. Martin gives data on prevailing power rates for all the major railway electrifications in the United States together with data on demand, energy consumption, load and diversity factors. These data indicate that the total demands are surprisingly small compared with the total connected loads, due to greater diversity as more mileage is included.

The motor-generator type electric locomotive has all-around desirable heavy traction characteristics and readily fits into the electrification picture. Power factor can be controlled within any desired limits. Power demand is rigidly controlled by means of train orders, limiting the synchronous amperes to any desired value, thus permitting full tonnage to be hauled on any grade at speeds proportional to the amount of power dispatched.

In regeneration, the motor-generator locomotive is superior to all other types in performance. About 40 per cent more tonnage per unit can be handled in regeneration service than can be handled continuously in hauling service up the same grade. Advantage is taken of this characteristic on the Great Northern by increasing train speeds in regeneration to the safe limits for track up to limits of substation capacity. This type of operation reduces the annual power cost by about 25 per cent, in addition to the intangible, but real savings on draft

Table II

	Rail Horsepower
5,400 Diesel	4,800
Steam Mallet *R 1* type	4,500 at 15–45 miles per hour
Electric locomotives *Y 1* (General Electric)	3,000 over speed range
Electric locomotives *Z 1* (Westinghouse Electric)	2,000 over speed range

Table III

			Gross Ton-Miles per Dollar
Steam	18.6	Gross ton-miles per gallon bunker "C" at $0.035 per gal.	= 530
Diesel	90.0	Gross ton-miles per gallon Diesel oil at $0.070 per gal.	= 1,285
Electric	6.2	Gross ton-miles per kwhr at substation at $0.0085 kwhr.	= 730

Table IV

	Steam *R 1*	5,400 hp Diesel	Electric
Fuel	59.53	40.36	35.00
Lubrication	2.02	6.20	.67
Engine house expense	7.71	9.93	4.46
Supplies	.46	.75	.38
Total operation	69.72	57.24	40.51
Repairs	41.82	20.26	17.83*
Total operation and maintenance—cents per mile	111.54	77.50	58.34

* Electric repair costs include rewinding of all main generators of 10 Westinghouse Electric and 8 General Electric and 2 spares.

gears, wheel, brake shoes and brake rigging. As an example, an electric motor-generator locomotive of the General Electric (*Y 1*) type capable of hauling 1,000 tons up a 2.2 per cent grade at 18 miles per hour, will hold the same train down a 2.2 per cent grade at 25 miles per hour, or could hold a 1,400 ton train down the same grade at 18 miles per hour. The power pumped back into the power system amounts to 25 per cent of the power purchased to pull that train up to the summit of the mountain from either end of the electrification. The recent purchase of the two latest engines of this type, which are the largest single unit locomotives yet built, confirm the satisfactory performance of the motor-generator locomotive in this service for the past 20 years.

The trend of the latest motive power design is toward the generator type, whether its prime mover be steam or gas turbine, Diesel, or synchronous motor. This would seem to be an opportune time for the various manufacturing companies to get together before further development of as many models as there are companies, all different and noninterchangeable. Some railroads are already operating large numbers of steam, electric, and Diesel-electric motive power. If as many parts as possible could be standardized, a railroad thinking of operating electric locomotives in the mountains, Diesels or gas-turbine locomotives across the plains or through the deserts, could carry one type of electric traction motor to fit all three, one wheel assembly common to all, perhaps a line of generators used singly or in multiple to fit any type of prime drive, with control and battery details enough alike so that one size of battery tray could be applied to all locomotives. Such a program would ultimately reduce the cost of all types of motive power. Any desired rail horsepower could be obtained by different make-up of wheel arrangement in power trucks, giving each railroad the chance to select the most suitable type required. Railway stores and manufacturers' service shops could reduce their stocks as one system could help any other one in an emergency. This would have meant a great deal during the war. Electrical engineers have been accused of being biased in favor of electrification. Since horsepower is all that counts in motive power, and since more horsepower can be built into electric locomotives than in any other type of motive power, it is natural to favor the system which provides the best tool, but only if it can be obtained unhampered by high first cost, and operated unhampered by high electric power costs.

The large investment and mileage existing on the Pennsylvania Railroad, New York, New Haven and Hartford, Norfolk and Western, Virginian, Reading, and Great Northern in 11-kv 25-cycle single-phase a-c electrification would seem to warrant consideration of this system as a standard. The motor-generator type locomotive seems to warrant consideration as a standard type locomotive. If such standardization for electric operation could be agreed upon by the manufacturers, and if this could be followed by a rational standardization of parts for other types of locomotives with electric drive, then electrification will again take its place and lead the field, if not shackled by high power costs.

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1. The Electric System of the Great Northern Railway at Cascade Tunnel, C. T. Hutchinson. AIEE Transactions, volume 28, part 2, 1907, pages 1281–1319.

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Discussion

T. F. Perkinson (General Electric Company, Erie, Pa.): The comparative maintenance costs given in Gaynor's paper are liable to be misleading without some explanation as to what is covered by the figures.

The steam and electric locomotives concerned are 15 to 20 years old, whereas the Diesel-electrics are not more than five years old—their average age is probably less than five years for the period 1941 to 1946. It is noted that the electric costs include the rewinding of all of the main generators. Undoubtedly if the Diesels were as old as these electrics, their main generators would have been rewound at least once and possibly twice.

The writer has seen maintenance figures for a fleet of some dozen 5,400-horsepower Diesel-electrics operated in a western mountain service comparable to that of the Great Northern, wherein the cumulative maintenance costs per mile had reached 36 cents for the years 1942 to 1946 and were still rising with age. Currently the oldest locomotives in this fleet are costing in the neighborhood of 55 cents per mile for maintenance. Monthly mileages average in the neighborhood of 10,000 per 5,400-horsepower locomotive.

It is assumed that the operating costs given are per locomotive mile. If this be the case, a more meaningful comparison would have been on the basis of a thousand gross ton-miles trailing tonnage since the rail horsepower ratings (and tonnage ratings) of the electrics are considerably less than for either the steam or Diesel-electric locomotives.

Erroneous conclusions are liable to be drawn when comparing "bare" operating costs for motive-power types as different as are the three involved in the Gaynor paper. In comparing Diesel-electric and straight-electric costs it should be borne in mind that the former generates its own power, while the latter requires transmission, conversion and distribution means for energy procurement after the power is delivered to the railroad. A fair comparison of motive-power costs should assess the electric with a proportionate share of the costs of operation and maintenance of these auxiliary facilities.

The comparison of different types of motive-power systems should include not only the operating costs of the motive-power units themselves, but also the costs of the facilities, materials, and labor, peculiar to each type of power and the cost of owning the plant as a whole, if the comparisons are to be meaningful.

H. F. Brown (New York, New Haven and Hartford Railroad, New Haven, Conn.): The author's paper is an interesting addition to the previous Institute papers on the Great Northern electrification. It is one of the few papers which have actually set forth steam, electric, and Diesel-electric motive power operating costs, as well as other comparative performance. The Great Northern is one of the pioneer electrifications of the United States, and any operating data coming from this railroad is worthy of careful attention.

It is noted that comparative fuel costs were obtained on the 2.2 per cent grade for all three types of power, as follows:

Steam	530 gross ton-miles per dollar
Diesel	1,235 gross ton-miles per dollar
Electric	730 gross ton-miles per dollar

This indicates electric power consumption at the rate of 162 kilowatt-hours per thousand gross ton-miles, which is about right for the type of "elevator service" performed. However, a large percentage of this power is

presumably returned to the power supply system on the down grade through regeneration. Since the other two forms of motive power do not have this truly economic feature, should not the electric operation be credited with the power returned, for a more accurate comparison? It is mentioned elsewhere in this paper that at least 25 per cent of the entire power used on the electrified run is returned to the supply through regeneration. Using only this proportion on the grade (actually, every bit of the power regenerated should be credited to the grade, and not to the whole run) the electric power cost of $0.0085 per kilowatt-hour would be reduced by 25 per cent, or to $0.0064 per kilowatt-hour, thus making the electric gross ton-miles per dollar about 930 instead of 730, as given.

The comparative costs of operation in cents per locomotive-mile would be more readily comparable if all were on a gross ton-mile, or a rail-horsepower basis. It is not clear whether the electric locomotive costs given are per unit cab, or for the normal units operated together, as in the evident case of the Diesel-electric. If per unit cab, it is obvious that an average of 2 units must be taken to equal the rail-horsepower of the Diesel-electric, and it is possible that the costs shown for electric locomotives should be multiplied by 2 for true comparison, which would make them in general, greater than the steam locomotive costs, which does not seem possible. Possibly Gaynor will be willing to be a little more specific on this point. It is to be noted, further, that electric locomotives 20 years old, with major repairs, are compared with the repairs to Diesels which are presumably not more than three years old, which, again, is not exactly equitable.

Also for over-all comparison of costs, the line maintenance costs should be included in some way with the other costs of electric operation. An interesting fact is noted that the fuel costs of both steam and Diesel-electric operation is about 53 per cent of the total operating and maintenance costs shown, while for the electric it is about 60 per cent.

Although Gaynor states that Diesel "fuel costs . . . are not affected by such artificial and adverse economic restrictions" as electric power costs, with their "demand" charges, it is a fact that electric power costs have, in general been decreasing over the years, in spite of increases in the cost of fuels. Whether that trend will continue remains to be seen. It is not likely, however, that oil will get any cheaper, and every indication today seems to point to more "artificial and adverse economic restrictions" in the cost of that fuel than any other now in use.

Gaynor has a point to be carefully considered in his plea for standardization of motors and other electric parts; on the other hand, motor-generator locomotives show up the best on mountain grade electrifications, and are not so economic as to performance and first cost on electrifications like the New Haven or the Pennsylvania. Further, it is not outside the realm of possibilities that the electric equipment on Diesel-electric locomotives may be alternating current before many years.

Joseph F. N. Gaynor: The per locomotive mile cost comparisons as used in the paper are standard yard stick here, where all of the other data for each type power is also known in terms of thousand gross ton-miles trailing for each. The paper was mainly one to show the superior performance of motor-generator locomotives, even when operating under restrictive power contract, against other types of motive power on maximum gradients.

A 230-Kv 3-Cycle Oil Circuit Breaker for Extra-Heavy Arc Rupturing Duty

W. M. LEEDS
MEMBER AIEE

G. B. CUSHING
ASSOCIATE AEE

Synopsis: As the ultimate generating capacity of Grand Coulee Dam is approached, designers of high voltage circuit-interrupting devices are challenged to produce circuit breakers capable of interrupting faults far in excess of the present maximum standard rating of 3,500,000 kva. An oil circuit breaker designed for extra-heavy arc rupturing duty is described in this paper. Results of varification tests made in the laboratory are compared with those obtained on subsequent field tests at Grand Coulee Dam. The close correlation of circuit breaker performance in the field and laboratory confirms the accuracy of the laboratory method of testing. With successful correlation up to 7,500,000 kva and laboratory tests simulating still higher rupturing duty, there is now a sound basis for design of oil circuit breakers with a rated interrupting capacity up to 10,000,000 kva.

THE highest rated interrupting capacity of any 230-kv circuit breaker now installed is 3,500,000 kva. At a number of locations in the United States and in Canada short-circuit interrupting ratings of 5,000,000 kva will be required shortly. However, the tremendous power concentration at Grand Coulee Dam involved in an ultimate installation of 18 generators of 108,000 kva and 12 synchronous pump motors of 65,000 horsepower each results in fantastic short-circuit values approaching 15,000,000 kva if all machines are connected together on a solid bus and all of the transformer neutrals are solidly grounded. This includes feedback from the Bonneville Power System with the ultimate number of transmission lines connected. Even with sufficient reactance in the transformer neutrals to limit single-phase short-circuit currents to the 3-phase values, some additional limiting impedance between bus sections is required to keep the maximum short circuits below 10,000,000 kva unless pumps and associated generators are isolated from the system. This paper describes the design features of an oil circuit breaker suitable for interrupting duty between 7,500,000 and 10,000,000 kva with 3-cycle interrupting time.

Among the features required of such a circuit breaker in addition to high-speed arc rupture and tremendous short-circuit interrupting ability should be included provision for operation on a 20-cycle reclosing duty cycle so as to improve the stability of the large interconnected system. Although circuit breakers for 230 kv are normally rated 800 amperes, a continuous current rating of 1,000 amperes is required for this application, since calculations show that currents of between 900 and 1,000 amperes may have to be carried through the line circuit breakers during emergency conditions when a line is out of service.

Theory and experience in connection with the de-energizing of long high-voltage transmission lines have shown the possibility of switching-surge voltages of several times normal unless the charging current is extinguished without more than one restrike defined as an arc reignition after a current zero pause of 1/4 cycle or more. Laboratory and field tests on the type of circuit breaker decribed in this paper have shown the ability to interrupt line charging current promptly without the complication of large voltage-controlling resistors.

Steel-Tank Circuit Breaker Design

Just a few years ago 230-kv circuit breakers with interrupting ratings of 2,500,000 kva were built in steel tanks 108 inches in diameter. However, the development of the multiflow type of circuit interrupter[1] has made possible the design of a line of oil circuit breakers with a very substantial reduction in tank diameter and oil volume. Circuit breakers with full 230-kv insulation are now being built in 84 inch diameter tanks while the reduced-insulation design for use on systems with effectively-grounded neutrals are produced in 78 inch diameter tanks. Since the amount of gas generated during arc interruption is relatively small with the latest type interrupters, the clearances between live parts and ground are determined largely by the insulation tests rather than the amount of rupturing capacity. For this reason the extra-heavy arc rupturing duty circuit breaker described in this paper operates entirely satisfactorily in an 84 inch diameter tank. A section view of one pole of the circuit breaker is illustrated in Figure 1. The development of the completely-sealed oil-impregnated condenser type bushing[2] has provided rugged insulated terminals of such small diameter that they do not themselves present a limitation in the size and shape of the circuit breaker tank. Adequate space is available for as many as two current transformers on each bushing.

The mechanical closing effort applied to the interrupters by the circuit breaker lift rod is very nearly the same as in the lower interrupting capacity circuit breakers which have been operating successfully for a number of years.[3] A pneumatic mechanism with a $9^1/_2$ inch diameter piston is used to close the circuit breaker (Figure 2). The mechanism lever system is arranged so that on normal tripping the piston remains connected to the circuit breaker pull rod. This permits rapid reclosure from any point in the mechanism stroke. However, if the circuit breaker is tripped immediately following the closing operation, the pull

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W. M. LEEDS and G. B. CUSHING are with the power circuit breaker engineering department of the Westinghouse Electric Corporation, East Pittsburgh, Pa.

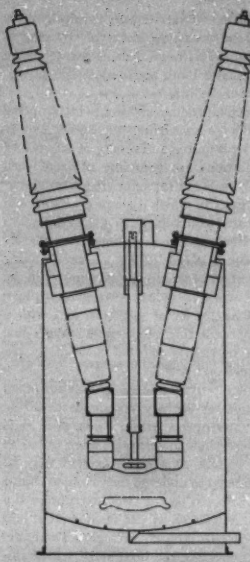

Figure 1. Section through single pole unit of 230-kv circuit breaker equipped with interrupting units similar to those tested to 7,500,000 kva at Grand Coulee Dam

Tank diameter is only 84 inches compared to older 3,500,000-kva circuit breakers with 108-inch tanks

rod is disengaged from the piston so that residual air pressure will not influence the opening speed. With such a mechanism the circuit breaker is fully equipped to operate on a modern reclosing duty cycle with reclosing intervals as short as 20 cycles. The multiflow circuit type of interrupter has shown remarkable arc-extinguishing effectiveness over a wide range of voltage applications. The particular design adopted for the high capacity 230-kv circuit breaker described in this paper is illustrated in Figure 3 and in section view by Figure 4. It will be noted that two arcs are drawn substantially simultaneously, gas pressure generated in the lower break driving oil up two channels and then through pairs of inlets so as to converge on the main arc in the upper break at a number of different levels along its length. As the oil flows upward and downward through orifices in which the arc is drawn, gaseous arc products and carbonized particles are swept out of the vents interspersed between the inlets. The cooling and deionizing action of this flow builds up dielectric strength with such rapidity at a current zero that the arc does not reignite and the circuit is interrupted.

In order to speed up the opening of low currents, such as transmission-line charging currents, a spring driven oil-driving piston is provided to supplement the normal self-generated flow. The oil is introduced through a multiorifice structure in the lower or pressure break before entering the main channels to the upper break. This provides maximum interrupting effectiveness when the piston starts to function after an intentional delay to allow the contacts to open far enough so that reignition will not take place after the very first attempt to clear. On high current interruptions the piston will be held by back pressure until after arc extinction when the grid pressure falls rapidly and allows a flow of clean oil to flush out the structure.

Equal division of recovery voltage between the two interrupters in a pole unit is assured by resistor columns paralleling each stationary contact assembly. These consist of carbon-impregnated ceramic blocks assembled in a pair of fibre tubes which are mounted on either side of the contact-operating rod. (See Figure 4.) The resistance value of 400,000 ohms per interrupter is low enough to perform its voltage-dividing function adequately and yet high enough that the small residual current of only 1/6 ampere is interrupted promptly when the circuit breaker cross-arm disconnects from the fingers at the bottom of the grid.

Large size resistors of only a few thousand ohms for the purpose of limiting overvoltages due to arc restriking during the interruption of line charging current have the disadvantage of a 25 to 50 ampere residual current which requires several cycles of arcing before interruption in the disconnect gap. The provision of an oil-driving piston which prevents delayed restriking of the arc eliminates the need for such a resistor.

A considerable margin of safety in interrupting ability has been demonstrated in a 230-kv 3-cycle 3,500,000-kva circuit breaker equipped with multiflow grids by field tests carried to 4,370,000 kva. These tests were reported to the AIEE in August 1946.[4] No limitation in interrupting capacity has been found as yet to the multiflow principle, the higher capacity interrupters requiring principally detail design changes in the oil flow control and an increase in ruggedness of construction. For instance, the phenolic-resin-impregnated wood tie rods holding the grid assembly together have been increased appreciably in diameter for the interruption of currents up to 25,000 amperes. The strain on these tie rods is kept down by designing the fibre structure so that a minimum of area will be exposed to the internal pressure.

Figure 2. Pneumatic operating mechanism capable of reclosing a large oil circuit breaker within an elapsed time of 1/3 second after a short-circuit current interruption

Figure 3. Multiflow grid with shields in place designed for extra-heavy arc rupturing duty at 230 kv

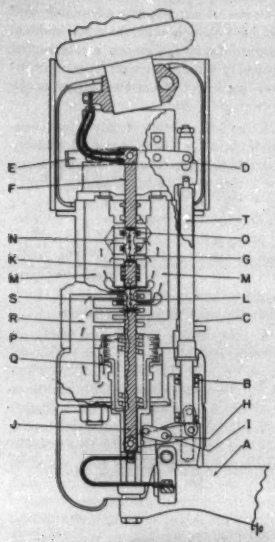

Figure 4. Section through 230-kv multiflow interrupter designed for extra-heavy arc rupturing duty

Tripping of circuit breaker releases main cross arm *A* allowing spring *B* to move operating rod *C* downward and rotate levers *D* and *E* withdrawing upper contact *F* and establishing arc *G* in the interrupting chamber. Simultaneously, pin *H* swings lever *I* downward, pulling lower contact *J* away from the intermediate contact *K* to draw pressure arc *L*. Pressure from arc *L* drives oil through channels *M* to multiflow inlets *N*, deionizing arc *G* in multiorifice structure and sending arc products out vents *O*. Spring *P* pushes piston *Q* downward to provide supplementary oil flow for low current arc interruption or scavenging flow after high current interruption, driving oil through channel *R* to orifice structure *S* in lower gap and then to main flow channels *M*. Resistor *T* assures equal division of restored voltage between the two interrupters of a pole unit following interruption of the short circuit

Although the primary consideration in extending the interrupting capacity is physical strength, the momentary current that must be carried by the contacts during the initial cycles of short circuit does run into a limitation for silver-tungsten butt-type arcing tips with relatively light contact pressure and small cross-section area. The introduction of finger contacts, greatly increased contact pressures, or more massive arcing contact elements all would result in a substantial increase in accelerating effort, and in the corresponding mechanism closing force, required to maintain adequate operating speed. For a 25,000-ampere interrupting capacity corresponding to 10,000,000 kva at 230 kv, the momentary current rating is approximately 34,000 amperes. This is higher than any similar butt-type contact momentary rating, and a radical change in design might be necessary if still higher momentary current ratings were required. However, if greater concentration of power in the future should necessitate higher rupturing capacity there are many factors to suggest going to a higher voltage rating which would keep the interrupted current and momentary current values within reasonable limits. The use of copper-alloy arcing contacts one inch in diameter has been selected to provide more conductivity and greater mechanical ruggedness for the heavy duty than the former aluminum and copper contact construction. Actually the moving mass is somewhat greater, and the accelerating effort has been increased accordingly.

High Power Laboratory Tests

The interrupting ability of the described circuit breaker was investigated in the Westinghouse A-C High Power Laboratory. Here the full power of the laboratory generators was used to demonstrate the interruption of high short-circuit currents. Also, the interruption of capacitive currents comparable in magnitude to those associated with the charging of transmission lines was demonstrated using a capacitor bank to simulate the capacitance of the line.

Adequate demonstration of interrupting capacity approaching 10,000,000 kva at 230 kv requires a diversity of laboratory connections not generally utilized when verifying lower capacity circuit breakers.[5] The limit to which a single pole unit can be tested at a voltage of 132 kv, equal to the line-to-ground voltage of a 230-kv system, is about 12,000 amperes, taking advantage of maximum asymmetry in the current wave. This represents a 3-phase fault at 230 kv of 4,800,000 kva with a freely-oscillating recovery voltage rising at a maximum rate of 3,400-volts per microsecond.

When provision is made to assure equal division of recovery voltage between the two interrupters within a pole unit, each of these single interrupters when tested on half voltage at a given current may be considered to undergo duty comparable to that imposed on the two interrupters of a pole unit tested at the same current and full line-to-ground voltage. Therefore, by connecting the laboratory transformers for 66 kv, short circuit currents up to 24,000 amperes can be delivered to a single interrupter, representing a 3-phase fault at 230 kv of 9,600,000 kva. The maximum recovery voltage rate of 3,200-

Table I. Interrupting Performance 230-Kv Circuit Breaker—In Testing Laboratory

Duty: O	Test Number	Interrupted Current RMS Amperes	Interrupting Time Cycles*	Equivalent 3-Phase Kva
Single Pole Unit (2 Interrupters) 132 Kv—1-Phase—60 Cycle	1	171	2.0	68,100
	2	142	2.2	56,600
	3	305	2.3	122,000
	4	274	2.2	109,000
	5	645	2.3	257,000
	6	619	2.4	247,000
	7	1,232	2.3	491,000
	8	1,355	2.3	540,000
	9	2,552	2.3	1,002,000
	10	2,480	2.3	987,000
	11	4,900	2.3	1,950,000
	12	4,920	2.2	1,960,000
	13	8,460	2.2	3,370,000
	14	12,100	2.4	4,820,000
	15	11,900	2.7	4,740,000
1/2 Pole Unit (1 Interrupter) 66 Kv—1-Phase—60 Cycle	16	774	2.5	308,000
	17	15,000	2.5	5,980,000
	18	24,400	2.5	9,720,000
	19	24,100	2.5	9,600,000
Single Pole Unit (2 Interrupters) 66 Kv—3-Phase—60 Cycle	20	8,430**	2.4	3,360,000
	21	7,970	2.2	
		9,750	2.2	
		11,580	2.4	4,610,000
	22	12,100	2.2	
		12,800	2.4	5,100,000
	23	594	2.7	
		810	2.4	323,000
	24	12,200	2.1	
		15,100	2.3	6,020,000
	25	18,900	2.4	7,540,000
		14,600	2.6	

* To obtain arcing time subtract 1.05 cycles.

** Tabulated for each interrupter; kilovolt-amperes based on the higher current.

Table II. Interrupting Performance 230-Kv Circuit Breaker—In Testing Laboratory

Duty: (CO—60 Cycles—CO)	Test Number	Interrupted Current RMS Amperes	Interrupting Time Cycles*	Equivalent 3-Phase Kva
Single Pole Unit (3 Interrupters) 132 Kv—1-Phase—60 Cycle	1	247	2.7	98,000
		247	2.6	98,000
	2	4,590	2.4	1,735,000
		4,690	3.4	1,860,000
	3	7,370	2.7	2,920,000
		8,450	3.3	3,350,000
⅓ Pole Unit (1 Interrupter) 66 Kv—1-Phase—60 Cycle	4	810	2.6	321,000
		860	2.7	343,000
	5	13,870	2.5	5,500,000
		12,320	2.6	4,880,000
	6	16,500	2.4	6,570,000
		18,450	2.5	7,300,000
	7	21,300	2.55	8,500,000
		18,150	2.65	7,200,000

* To obtain arcing time subtract 1.05 cycles.

volts per microsecond on one-half the pole unit corresponds to 6,400-volts per microsecond on the complete circuit breaker.

In order to confirm the ability of all of the circuit breaker pole-unit components to withstand the mechanical stresses resulting from simultaneous arcing in both interrupters, the 3-phase connection at 66 kv illustrated in Figure 5 provides higher equivalent short-circuit power than can be obtained directly on the full pole unit. It will be noted that one phase from the delta-connected transformers is brought to each terminal of the pole unit, and the grounded third phase to the pole unit cross arm through a flexible connection. By this means current is supplied to each interrupter corresponding to an 8,000,000-kva 3-phase fault on the complete circuit breaker, and with recovery voltage conditions comparable to that encountered in 230-kv service.

The single pole test circuit breaker, equipped with high-capacity multiflow interrupters, was tested under each of the three conditions just described, each test circuit adding something to the complete picture of interrupting performance. Test results are tabulated in Table I and II. Interrupting time has been plotted as a function of current in Figure 6. Uniformity in interrupting time throughout the current range is apparent. Also evident is the similarity in performance obtained using the three different laboratory connections.

Outstanding performance is demonstrated in test 18, Table I. With this operation an equivalent of 9,720,000 kva was interrupted 2.5 cycles after the trip coil was energized. Following this test a second interruption, test 19, Table I, of 9,600,000 kva was accomplished in the same interrupting time. Figure 7*a* is the oscillogram taken in conjunction with the former of these tests. Oscillograms Figure 7*b* and 7*c* pertain to test 14 and test 25, Table I, respectively which are typical records of circuit breaker performance using the 132-kv single phase and the 66-kv 3-phase laboratory test circuits.

In preparing for field tests at Grand Coulee Dam it was understood that a reclosing duty cycle with a 60-cycle time interval between interruptions was to be specified, and a number of tests were made to determine the effects of such duty on the circuit breaker performance. In general there appeared to be very little difference in interrupting performance, between the first and second interruptions. However, in the current range around 6,000 amperes under the high voltage recovery rate of the laboratory (3,200-volts per microsecond) the interrupting time of the second interruption occasionally extended beyond three cycles, due apparently to delay in building up full oil-driving pressure because of the residual gas cushion from the first interruption. Data from these tests are tabulated in Table II. The maximum equivalent kilovolt-ampere interrupted in the laboratory during a 60-cycle reclosing duty cycle is tabulated under test 7, Table II. In this test the interruption of an equivalent of 8,500,000 kva was followed by reclosing and immediate interruption of 7,200,000 kva. This is believed to be the first time that such enormous interrupting capacity has been demonstrated on a reclosing duty cycle either in the laboratory or on an operating power system. Figure 8 is the oscillogram of this test.

The ability of the interrupter to meet the conditions imposed when switching capacitive circuits has been thoroughly tested at currents of 45, 90, and 165 amperes with normal operating voltage applied across a single interrupter. Of a total of approximately 125 tests all interruptions were accomplished in less than three cycles, extinction occurring at the current zero following the initiation of forced oil flow. An extremely low probability of even a single delayed restrike was demonstrated, and in no case were voltages higher than double the peak voltage of the circuit experienced.

Field Tests at Grand Coulee Dam

While a large laboratory is a versatile tool in the development of high-power circuit breakers, because of the high interrupting capacity of the described circuit breaker, there are two service conditions which are not fully duplicated by laboratory tests and may be proved more reliably in the field. Only with the generating capacity which is available at

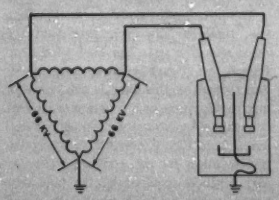

Figure 5. Schematic diagram of 3-phase laboratory circuit used for testing a single pole unit of a 230-kv circuit breaker at higher equivalent short circuit power than can be obtained by single-phase test

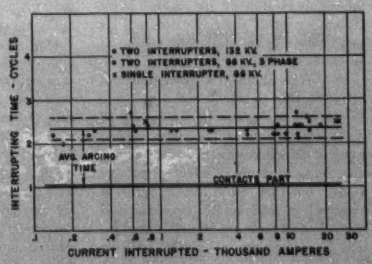

Figure 6. Interrupting performance of the test pole unit of a 230-kv circuit breaker as determined in the High Power Laboratory using three different circuit connections

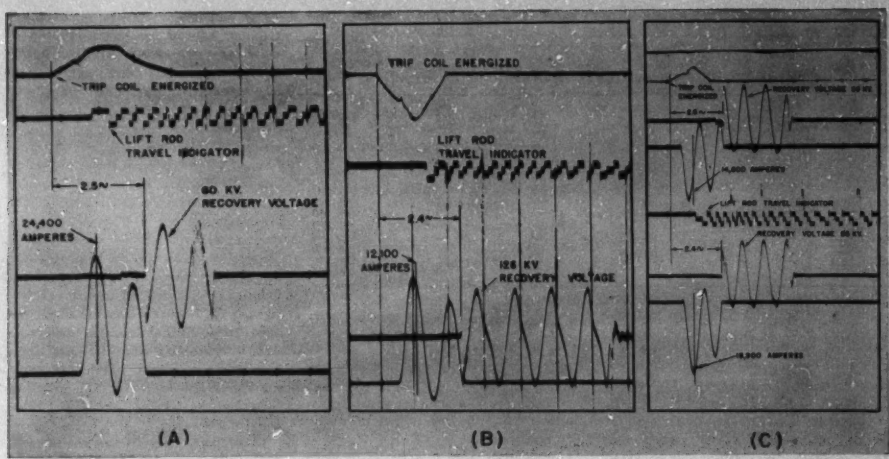

Figure 7. Oscillograms of the interruption of laboratory test circuits which approximate voltage conditions of a 230-kv system

a. 24,400 amperes interrupted by $^1/_2$ pole unit on 66-kv laboratory circuit
b. 12,100 amperes interrupted by full pole unit on 132-kv laboratory circuit
c. 18,900 and 14,600 amperes interrupted by the two interrupters of a full pole unit using 66-kv 3-phase connection shown in Figure 5

Grand Coulee Dam can a full pole unit be tested to an interrupting capacity of 7,500,000 kva with full line-to-ground voltage restored between one terminal and tank. Accordingly, field tests give better evidence that gas which is generated during interruption will not disrupt the dielectric strength between live parts and ground to the extent of causing flashover. Also, only under field conditions can all of the interrupters within a pole unit be tested in series at line-to-ground voltage at this high interrupting capacity. However, circuit breaker design factors are established and checked in the laboratory with sufficient accuracy to justify confidence when field testing at extended ratings.

On the other hand, it is characteristic of field tests that the rate of rise of recovery voltage on high-power short circuits is quite low because of the added capacitance of the interconnecting transmission lines and the damping effect of parallel loads. For this reason caution must be exercised in interpreting successful field tests at one location as complete verification of performance at another location where the recovery rate is appreciably higher. It is evident, therefore, that the best over-all picture of circuit breaker performance in the higher range of interrupting capacity ratings is given by the combination of laboratory tests at high recovery voltage rates up to the maximum available power and field tests at full rated capacity with the maximum available rate of rise of recovery voltage.

Although it is impractical to reproduce rigorously transmission line phenomena within the laboratory, the switching of large capacitor banks does approximate circuit breaker performance when opening line charging currents. These results in general are confirmed by transmission-line switching tests, which also add data on the actual amplitude of voltage surges due to traveling waves on the lines.[6]

A complete account of tests conducted at the Coulee Dam switchyard is presented in a companion paper.[7] Briefly two series of short-circuit tests were made on the new type *GM* circuit breaker pole unit. The first comprised 15 close-open (CO) operations, 12 of which were included in standard CO-15 second-CO duty cycles. The second series consisted of seven complete reclosing duty cycles, each with an initial fault interruption, then reclosure within an interval as long as 60 cycles and as short as 18 cycles followed by a second clearing of the short circuit. In addition there were seven tests opening an arcing fault in which the arc was not re-established even though the circuit breaker was reclosed within as short an interval as $12^1/_2$ cycles.

The extremely high interrupting capacities demonstrated are unparalleled among field tests. One test equivalent to the interruption of a 3-phase 7,500,000-kva fault was accomplished in the unusually short time of 1.85 cycles. After a pause of 15 seconds a second CO operation interrupted the equivalent of 7,340,000 kva without damage to the circuit breaker.

Among the 60-cycle reclosing tests was one interruption equivalent to 6,000,000-kva 3-phase, while the 18-cycle reclosing operations were carried up to 5,130,000 kva without the least external evidence of stress. There was no apparent difference in the arcing time for first or second interruptions, the total time from energizing the trip coil to arc extinction varying between 2.1 and 2.65 cycles for all of the higher power tests. From this data it is evident that sufficient oil is available in the grid structure to interrupt at least two heavy short circuits in rapid succession without the necessity of complete replacement of gas with fresh oil.

The interrupting time obtained in both the standard duty-cycle tests and the rapid-reclosing tests has been plotted in Figure 9 for each current interrupted. Superposed on this plot is the curve of interrupting time as a function of current determined by laboratory tests and transferred from Figure 6 with a small correction to allow for the additional time to part contacts required by the pole unit tested in the field. It should be noted that the average interrupting time for the points plotted from field tests data is discontinuous, indicating a change in the circuit by the addition of transmission

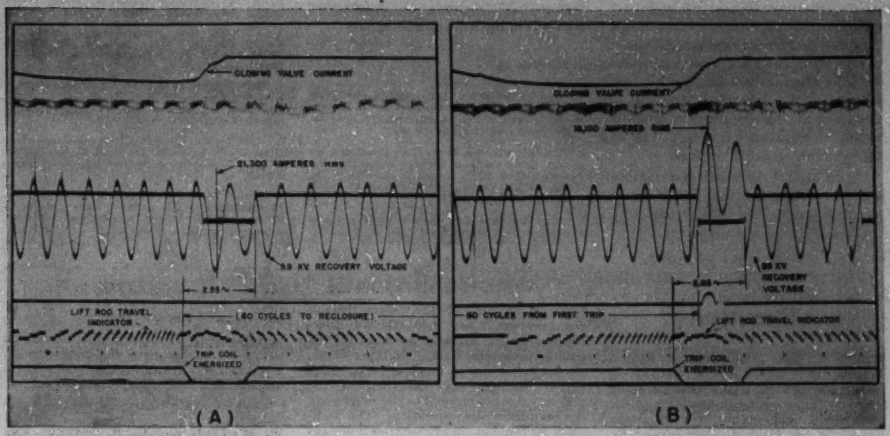

Figure 8. Oscillogram of a reclosing duty cycle test on a laboratory circuit approximating the current and voltage conditions of a 230-kv 3-phase fault of 8,500,000 kva

A single interrupter constituting half of a circuit breaker pole unit interrupts at 66 kv a current of 21,300 amperes, recloses the circuit in one second, and promptly interrupts the re-established current of 18,150 amperes

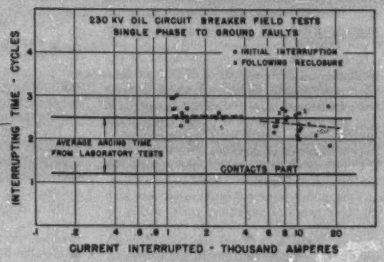

Figure 9 (right). Interrupting performance of the 230-kv circuit breaker tested at the Grand Coulee Dam

Dashed line, representing average interrupting time is discontinuous where transmission lines of the Bonneville Power System were connected to obtain additional short circuit power, reducing the voltage recovery rate. The bus voltage was lowered from 241.5 kv to 238 kv (respectively 140 kv and 138 kv to ground). Solid line represents average interrupting time from the laboratory data of Figure 6

lines which resulted in a reduction in the recovery voltage rate from approximately 1,200 to only 250 volts per microsecond. Three of the interruptions at 1,200 amperes showed somewhat longer interrupting time than average, two of these having followed as many as 10 earlier operations interrupting in excess of 4,000,000 kva.

Field data are closely correlated with results obtained in the laboratory. For instance, in the current range below 3,000 amperes where an isolated bus energized by one or two generators supplied the fault and provided a high recovery voltage rate, the interrupting times for laboratory and field tests were almost identical. A downward trend in interrupting time as the current is increased is attributed primarily to the corresponding decrease in recovery voltage rate, which is typical of field conditions where higher currents are obtained by interconnection to more remote sources of power. Laboratory circuits on the contrary provide maximum recovery voltage rate on the maximum current tests where the inductive reactance is a minimum and the capacitance to ground relatively unchanged. The slightly shorter average interrupting time shown on the highest power tests in the field therefore was to be expected. In general, the close agreement of laboratory and field results appears to justify in a very gratifying way the method used for laboratory verification of interrupting capacity.

Conclusions

A comprehensive investigation involving both laboratory and field tests has demonstrated that the multiflow principle of arc interruption can be utilized on high-voltage applications considerably beyond existing circuit breaker ratings. The design described in this paper has been verified by direct field test up to an interrupting capacity of 7,500,000 kva and by laboratory test to the extreme limit of the facilities, or 9,700,000 kva. Arc-rupturing performance has proven adequate on a high-speed reclosing duty cycle in which the reclosing interval was reduced to 18 cycles, approximately the minimum considered for 230-kv systems. Furthermore, the ability of the pneumatic mechanism to reclose the circuit breaker if necessary within intervals as short as 12 cycles was demonstrated.

While no limit has been reached in the interrupting capacity to which multiflow type interrupters may be adapted, the momentary current-carrying capacity of light-weight high-speed contacts imposes a practical limitation of approximately 25,000 amperes rated interrupting capacity. This corresponds to a 3-phase interrupting capacity of 10,000,000 kva at 230 kv.

Field tests on a complete pole unit of a

circuit breaker provided with means for controlling the division of voltage among the individual breaks have confirmed laboratory results with half a pole unit tested at half voltage, establishing further confidence in the laboratory practice of extending the current range of tests by testing subdivisions of a pole unit at appropriately reduced voltage.

On the basis of the extensive laboratory and field testing described in this paper, oil circuit breakers may now be built with confidence having interrupting ratings as high as 10,000,000 kva at 230 kv for either standard or reclosing operating duty.

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Discussion

Robert H. Nau (University of Illinois, Urbana, Ill.): Most of the material in Leeds' and Cushing's paper has naturally been concerned with a presentation of the test results and general over-all design features of a high-power circuit breaker for 230-kv performance. They have mentioned this sizeable reduction in steel tank diameters which the multiflow deion grid has made possible.

I should like to emphasize some of the fundamental design features of this particular multiflow grid design which will indicate the evolution of thought on this type of grid and also indicate possible refinements in the future. These details have been gathered at various times in discussions with Frank Fry, a colleague of mine on the faculty at the University of Illinois.

This grid is inverted in comparison with all other types of multiflow grids, which results in advantages both in oil flow from the driving piston and in the mechanical loading of all members because all the accelerating springs for the grid are connected through short and relatively light metal members to the circuit breaker crossarm.

The orifice through which the arc is drawn is actually $^1/_4$ inch smaller in diameter than that for the existing lower kilovolt-ampere capacity circuit breakers. This reduction in orifice area helps materially in the design of the oil driving piston and was made possible through a study of the orifice area requirements for various arc currents.

While the length of the present unit has remained substantially the same as that for the existing 230-kv 3,500,000-kva interrupters, the grid diameter has actually been reduced by several inches. In other words, although the kilovolt-amperes to be interrupted has been more than doubled, the grid was actually reduced in size in its more critical diametrical dimension. This indicates the trend in modern controlled designs for high-capacity circuit breakers.

P. L. Taylor (Allis-Chalmers Manufacturing Company, Boston, Mass.): I want to compliment the authors on their work in outlining such a marked extension of the range of interrupting capacity of power circuit breakers in the transmission class.

The effect of the tripling of the range of available interrupting capacities at the higher transmission voltages upon power system design seems almost certain to be far reaching.

It is peculiarly fitting that these tests indicating the feasibility of oil circuit breakers up to 10,000,000 kva were made in 1947, the fiftieth anniversary year of the first oil circuit breaker, which was designed by Dr. Leonard Elden in 1897. That these fiftieth anniversary oil circuit breakers are pneumatically operated and that some of the earliest designs of the Elden oil circuit breaker were also pneumatically operated, is worthy of note. The comments of Rietz in a companion paper[1] with regard to oil circuit breakers seem to me to be quite significant. He has said in part

"The modern tank type circuit breaker represents a product of years of evolution that in itself bespeaks a series of improvements that ought to reflect in capacity beyond its rating."

It has been our experience also, that the oil circuit breaker has a way of coming through.

Again I want to compliment the authors on their most important contribution.

W. M. Leeds and **G. B. Cushing:** The authors appreciate very much the supplementary discussion presented by both Nau and Taylor. The confidence expressed in the high-voltage dead-tank circuit breaker designs is very heartening, confirming our judgment that this type of switchgear provides the best solution to the heavy arc rupturing capacity problem in the high voltage outdoor circuit breaker field. It is interesting to note that in spite of the availability of the porcelain-clad impulse circuit breaker for some 13 years, manufacturers of this equipment have seen fit to bring out quite recently a completely redesigned line of steel tank circuit breakers to meet the same ratings as the oil-poor apparatus. We believe that we are best meeting the needs of the electrical industry by concentrating our efforts on only one general design of high voltage oil circuit breaker. We have endeavored to combine the advantages of a stored energy arc rupturing device, not requiring large control resistors for switching line-charging currents, with the ruggedness of the steel tank structure and the moderate operating load requirements of a two-unit-per-pole self-generating oil-flow type of interrupter capable of clearing rapidly and easily short circuits of the highest power available.

Are the Overhead Distribution Costs Retarding Railroad Electrification?

L. W. BIRCH
MEMBER AIEE

Synopsis: If terminals, suburban lines, and open-pit mining developments are excepted, the electrification of heavy haulage on the North American continent has progressed rather slowly for the past 15 years. In the meantime, and particularly postwar, European countries definitely have announced their decision to make extensive additions to their electrified railroads. Similar decisions have issued from other countries. No doubt, there is a satisfactory reason for this slowing down of electrification in North America. Based on the thorough and efficient operating methods followed by the railroads of North America, it may be concluded that economics may play some part in this "slowdown." Possibly the first cost of electrification is too high; if so, how can this first cost be reduced, or be made comparable to installations involving other motive power? It cannot be said that the high first cost of electrification is entirely due to the cost of the overhead distribution system. But, it can be said that the overhead system, as a part of the electrification plant, may contribute to high costs. It is not conceded that unfavorable first costs of electrification are the deciding factor in favor of some other mode of transportation, but a reduction in the first cost of electrification certainly will place the electric locomotive in a more favorable place. This paper describes some of the new overhead equipment, also system and structure designs, that have been developed in very recent years. The paper also calls attention to the many methods of cost reduction possible with many of these designs, also obtainable by following modern erection practices and by reducing engineering costs.

IF overhead distribution costs are retarding railroad electrification, what can be done to reduce these costs? Transportation is vital to our very existence. Without good transportation we would be struggling along in isolated communities, with few of the necessities of life and with none of the luxuries. Transportation has developed man, our country, and industry. There is nothing new about this statement. But the demands on transportation are ever changing. And now our vast country developed and expanded by transportation, demands that transportation keep pace with this development. Today, it is not enough to carry a passenger from Montreal to Vancouver, or from New York to San Francisco in four days. Besides speed the passenger wants comfort, a smooth ride, and safety. The shipper emphasizes speed in his demands, because of smaller inventories, nation-wide consumption of perishables, and sudden emergencies such as produced by World War II. Any old schedule and any old equipment will not be tolerated.

Our railroads have gone a long way toward meeting these demands since the Baltimore & Ohio Railroad initiated steam service in 1828, since the Santa Fe headed west from Topeka in 1869, and since the last spike was driven on the Canadian Pacific's transcontinental route in 1885. The railroads have developed higher average speeds, have furnished the passenger with many comforts of travel, and have given the shipper better service at lower rates.

The electrification of railroads can carry on this program still further. Then why is electrification in North America progressing at a snail's pace? Possibly the question should be reversed. Why are South America, Switzerland, and Sweden electrifying? Why are Belgium, Austria, France, Spain, Africa, and India making definite plans for extensive electrification? Of course, the true answer is one of economics and the principal factors affecting the ultimate selection of motive power are the cost of fuel and the capital expenditures for locomotives and overhead equipment which is approximately one-fourth of the total cost of electrification. It is the purpose of this discussion to analyze the distribution system and suggest means for reducing its cost.

Basis for Overhead Cost Reduction

The present-day competitor of electrification is the Diesel locomotive. Although the difference in first cost of a Diesel installation and an electrification may just equal the cost of the overhead system, we cannot say that in order to justify electrification the overhead system must be obtained free of cost. If a reduction in costs is necessary to balance the first cost of electrification with that of dieselization, then all factors in the estimate must bear their proportionate decrease.

The method for determining the per cent reduction of overhead costs in this paper is based on averages of three estimates. Incidentally, one of the electrifications for which one of these estimates was made is practically finished. The three estimates include a large industrial 3,000-volt d-c project; a long wood-pole single-track 11-kv Western railroad project, and a large Eastern multiple-track project. The increase in costs of electrification over dieselization were found to be 0 per cent, 16 per cent, and 37 per cent in the order named. The per cent cost of the distribution system based on the total cost of electrification was found to be 26 per cent, 27 per cent, and 28 per cent in the order named. It was fairly well established from these last percentages that the overhead system averaged only 27 per cent of the total cost of electrification. We are interested in reducing this 27 per cent factor. Now the industrial estimate showed no savings in first cost for the Diesel, the western road

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L. W. Birch is engineer, transportation department, Ohio Brass Company, Mansfield, Ohio.

Figure 1. Work train cost always has accounted for a huge portion of the total labor cost of electrification

16 per cent, and the eastern road 37 per cent. Since we are particularly concerned with the single- and double-track electrifications, an average of these latter percentages, 18 per cent, should be satisfactory for our example. By simple mathematics it can be easily determined that this 18 per cent increase in first cost of electrification over dieselization requires a 15 per cent reduction in electrification costs—straight across the board—if first costs are to be equated. If operating costs are to be considered this drastic reduction may not be necessary to equalize costs, but for the sake of illustration this procedure will be undertaken in order to assign the 15 per cent cost reduction to the distribution system. It is not claimed that this figure is accurate for all installations, and it is not implied that other factors of cost can be adjusted by exactly the same per cent. This example is given to call attention to a possible goal for cost reduction in the overhead system. Now, let us review the recent developments in overhead design and construction and see if this amount of cost reduction is possible. If so, we may conclude that overhead distribution costs are *not*, as an individual unit, greatly retarding electrification.

Prewar Effort to Reduce Cost

Immediately following the depression and continuing until stopped by the war, several of the large electrical manufacturers aided by a representation from steel fabricators, cable manufacturers, and overhead system manufacturers, formed a committee of engineers to study overhead distribution with one primary thought in mind—to reduce costs. This work was not restricted to materials alone, but involved considerable construction study, particularly from the standpoint of off-track equipment. It was recognized by this committee that work train cost has always accounted for a huge portion of the total labor costs. Also, based on the experience of the engineers participating in this work, it became quite evident that another reason for some of the high costs of the overhead system was attributable to the many and varied distribution systems found on our various railroad electrifications that were accomplishing the same purpose. No doubt, high development costs accompanied these installations. Duplicate designs could have been chosen for most of these electrifications. Again, it was found that many steel structures had been designed "on the heavy side." In many of these cases the hand of the railroad's bridge department was recognized. There seemed to be a great variation in the choice of cables, not as serious a fault as too much steel, but representing thousands of hours of study on the part of a group of engineers.

A recent and exhaustive study of the catenary span to determine the cheapest combination of cables for a given load and a given span length included a total of 20 different spans. These involved composite cables of various sizes and capacities, also several different sizes of auxiliary and trolley wires. What was the outcome of this study? Almost nothing. Any combination would have been satisfactory from a cost viewpoint. The final choice could not be made on the basis of cost.

The committee also found a wide variation in the selection of hardware. This may not represent a big increase in overhead system costs, but somebody had to pay for development, and development charges, whether made by the railroad or the manufacturer, are readily recognizable in the totals. The committee believed that engineering costs were too high on many electrifications. One prevalent cause for high engineering costs was due to the vast amount of steel structure

Figure 3. Wood structure in Utah supporting same weight-per-foot of catenary as Nevada structure (Figure 2)

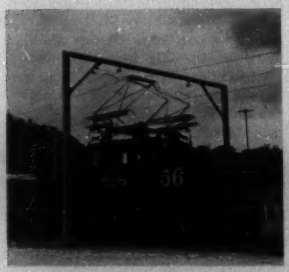

Figure 2. Steel catenary structure in Nevada (see Figure 3)

Figure 4 (left). Steel portal structure built with shapes

Figure 6. Two-track bracket on Montreal Terminals electrification

detailing by the electrification engineers. After all, the fabricator duplicates this work. He must for his own shop. Then why provide the fabricator with steel details? Just give him the ruling dimensions, limitations, and space diagrams and he will provide the finished design.

Stopping at this point would probably leave you with some conception of the possibilities of cost reduction. However, a discussion on catenary installations during the past 8 years may serve further to emphasize this subject, particularly by showing that further cost reduction is possible.

For the sake of clarity, this discussion of cost will be segregated into three main subjects. Each will be treated separately.

There is one division of overhead system costs that is familiar to all railway electrification men and this may be termed

1. *Standard costs* for want of a better name. The standard costs include direct labor and material for structures, catenary, switching equipment, track bonding, and other accessories contingent to the construction of the distribution system. Another division of costs may be placed in a second classification known as

2. *Clearances* which covers all anticipated expenditures for special designs considered necessary for signal, communication, and overhead bridge interference, also for the unusual headroom demanded by some companies. Another important item of cost may be called

3. *Psychological barriers*, a term which seems to be somewhat meaningless; however, this cost classification represents considerable money in every estimate. It deals chiefly with planning and engineering and is swollen in direct proportion to the ability of the engineer in charge to enlarge and glorify his organization.

Standard Costs

These costs include all labor and material costs for the structure, the catenary system, switching equipment, track bonds, and other contingencies that are necessary for the construction of the distribution system. They do not include the cost of office engineering, layout, inspection, signal changes, highway bridge and tunnel alterations, moving other wayside structures, and other innumerable items that would not be considered if the electrification were installed on a newly built railroad.

Structures

Although two electrified systems may be identical, the structures are apt to be different. Criticism of dissimilar structures on identical systems cannot be made until all conditions are known. Costs of material and labor, at the time of construction, greatly affect design. An electrification in Nevada during the depression was built with steel structures, 12-inch *CB* sections having been selected for the columns. Steel was cheap and labor costs were low. Later an equivalent installation in Utah was built with wood, construction costs having increased to influence this change. A similar situation is to be found in Canada. An earlier installation was built with steel while a subsequent electrification of steam-road tracks was accomplished with wood. In both examples the structures are supporting equivalent mechanical loads and maintenance costs are about the same.

Since this discussion deals chiefly with the lighter types of distribution systems, for 1- and 2-track sections rather than with the heavier types, four cost comparisons of 2-track structures may serve to illustrate the problem of structure economy. First a breakdown will be provided for two types of steel structures spaced 300 feet apart and carrying a

Figure 5. Steel cross-span structure on outside haulage of Pittsburgh Consolidation Coal Company

Figure 7. Wood cross-span construction, Canadian National Railways

Figure 8 (left). The handling cost of structures may be greatly reduced by the elimination of a certain amount of storage

Figure 9. Two-track portal structures with laced truss and columns usually cost more than structures built of shapes

catenary system of 3 pounds per foot. The cost of fabricated steel is 5 cents per pound.

	Steel Portal	Steel Cross Span
Cost of steel	$400	$250
Cost of cross-spans		65
	400	315
Labor, erecting	120	110
	520	425
Concrete footings	350	210
Guys		330
	$870	$965

The comparison shown in the foregoing tabulation was not provided for the purpose of selecting the cheapest structure. It was reproduced to show the breakdown of costs, which includes only structure items, and not any portion of the distribution system such as cables and hardware. To illustrate structure costs and to make a reasonably accurate comparison, two additional types of structures will be added, one of which is wood. Only the total costs will be given in Table I since the first tabulation has provided the breakdown.

In this table the increases in cost are progressive. The costs shown in the last line include all of the increases described in the left-hand column. Incidentally, these figures actually show the increases in labor and material for the last two years.

Returning to the two references concerning the initial building with steel and the subsequent building with wood, the comparison of costs for the portal structure and the wood cross span show that with 5 cent steel the portal is slightly cheaper than wood and with 7 cent steel, higher concrete cost, and higher labor cost, the wood pole structure is cheaper. However, if the comparisons are analyzed closely it will be observed that the differences in total cost for the steel structures are small. This would lead one to believe that the choice of steel structures is not necessarily based on first cost.

If it is agreed that the first cost of a structure does not materially affect its choice, with the possible exception of wood structures, a reduction in cost scarcely can be anticipated by the selection of design. Reductions in costs must come from some other source. If we analyze the large divisions in Table I, we can select several items which, if reduced, will materially affect cost. These are

A. Handling of structure.
B. First cost of material.
C. Source of material.
D. Field labor.

(*A*). The handling cost of structures may be reduced greatly by the elimination of a certain amount of storage or warehousing. It is frequently possible to schedule the manufacture of structures in such a way as to permit the erection of the structure direct from the railroad car on which it was shipped. If this can be done, the storing, sorting, and reloading charges are reduced. It is recognized that this saving may be difficult, particularly at the present time, and it is realized that in order to accomplish this saving it may be necessary to resort to a certain amount of car demurrage charges which today are not to be tolerated. But, during normal times, handling charges may be reduced by this procedure.

(*B*). The tabulations show a structure cost for steel at 5 cents a pound, also for steel at 7 cents a pound. Although this is approximately the spread in the cost of steel during the past two years, it is also a common differential between the cost of fabricated shapes and structures fabricated with lacing. The cost of steel can be greatly reduced by the choice of fabrication. Briefly, structures built with H-sections (wide flange or *CB*) reduce cost, while structures fabricated with corner angles and lacing increase costs. There are many reasons why laced columns and beams must be used, in some cases to reduce weight and in other cases to secure strength, but in general the simple shape such as the H-section, for both the column and the beam is to be favored for 2- and 3-track portals.

(*C*). In general, structure cost can be reduced by securing the material within a reasonable distance of the point of application. This is certainly true for wood. A western red cedar pole transported from the West Coast to the East incurs a large freight cost. This is also true of a southern pine pole that is carried too far into the West. Ultimately, cost is re-

Figure 10. The 2-track portal structure built with shapes is usually quite economical, Delaware, Lackawanna and Western Railroad

Figure 11. To reduce field labor, brackets, cross arms, and insulators may be attached to the pole. Then, the entire structure may be erected as a unit

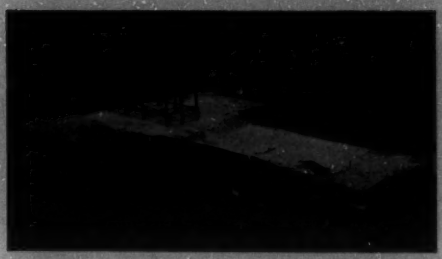

Figure 12. The shallow hole for this structure can be dug with off-track equipment while the deep hole usually requires hand digging

sponsible for the selection of the structure. A local product is cheaper than a product that is transported a long distance.

(*D*). Another means of reducing structure costs is to eliminate as much field work as possible. Field work is expensive, particularly because it is maintained at a disadvantage. Track availability, portable shops, and the general delays incident to outdoor work greatly increase the cost of field labor. Therefore, if possible, as much fabricating as consistent should be accomplished in the plant of the builder. This is also true for wood construction since it is possible to frame the poles before distribution. Brackets, crossarms, and insulator strings can be assembled units for quick attachment to the pole.

A single-track railroad offers a decided handicap to the construction crew since track availability is not sufficient to permit good efficient timing. In an 8-hour shift the track may not be available over two or three hours for the operation of a work train. On a double track railroad the availability may not be much better. Therefore, off-track equipment becomes a suggested solution. Although there are many sections where off-track equipment cannot be applied, most railroads are built over sufficient flat country to permit from 40 to 85 per cent erection with off-track equipment.

If steel is shipped from the fabricator on flat cars, ready for erection, there is not much to be gained by transferring the steel to the ground and erecting with a tractor-crane. However, it may be considerably quicker and cheaper to distribute wood poles from a supply train or truck and erect with the off-truck crane.

Excavations for steel structures may be broad and shallow or narrow and deep. This depends on the structure type, whether pin-end or fixed-columns are to be erected. The fixed type requiring a side bearing footing necessitates a comparatively deep excavation while the pin-end structure can be set on a shallow flat footing, thereby eliminating the necessity of digging a deep hole, possibly in a fill consisting of broken rock. The type of structure is based on design which in turn is influenced by cost. Several recent estimates indicate that with present-day material and labor costs, the pin-end type of structure is the cheapest, if the concrete footings are taken into consideration. The shallow hole can be dug with off-track equipment while the deep hole usually requires hand digging.

For wood poles, it is possible to use a boring machine in certain portions of the country, particularly the plains and desert sections, but, as a general rule, the boring of holes does not show a saving over hand digging.

Heretofore, concrete trains have been used on many large eastern electrifications. The concrete train involves the carrying of cement, water, sand, and gravel. It also involves lost time due to interference with regular trains. Some recent estimates indicate that this method of handling concrete is not quite as efficient and not quite as cheap as the centralized ready-mix plant which may be located at intervals of 5, 10, or 20 miles along the electrification. If the centralized ready-mix plant is located at 10-mile intervals, the longest haul is in the vicinity of 5 miles and this may be made with either a truck or a comparatively high speed train consisting of a light engine, Diesel or steam, and sufficient flat cars to carry the trucks. The same concrete truck that is used for the highway can be loaded on the flat cars, or several revolving mixers can be permanently mounted on flat cars. The principal saving with this arrangement is in the hauling of raw materials, also in the time required to carry the concrete to the excavation. There is greater track availability if the short train is used and in many districts trucking the concrete saves time. It has been estimated that as much as $5.00 per yard of concrete and

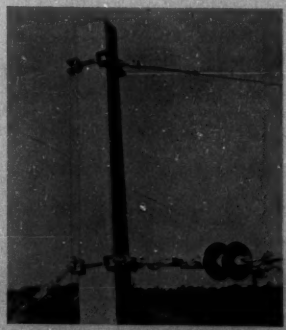

Figure 13. Cast attachments on concrete pole for spans and guys

Table I. Summary of Costs of 2-Track Structures for 300 Feet of Electrification

	Steel Portal	Steel Cross Span	Steel Bracket	Wood Cross Span
For steel at 5¢ per lb	$ 870	$ 965	$ 910	$ 890
For steel at 7¢ per lb	1,030	1,065	1,030	890
Cost of concrete and excavation increased $5 per yd	1,080	1,125	1,100	890
Labor increased by 50 per cent	1,140	1,180	1,160	1,025

Figure 14. All span and insulator attachments to this steel structure are made with adjustable clamps

Figure 15. Catenary span using a main messenger, an intermediate messenger, and two contact wires on Canadian National Railways

2,400 volts direct current

excavation can be saved with this procedure.

Attachments

Heretofore most attachments to steel structures have been made with fabricated shapes usually manufactured by the structural steel company. For permanent locations on the structures this method of attachment is quite satisfactory; however, there is always the necessity for adjustment and alignment. Clamped attachments for the support of insulator strings, pulloffs, and steadies provide a means of adjusting these assemblies. If the assemblies can be quickly adjusted to their final position there is a saving in field labor. Also, clamped attachments eliminate the necessity of drilling the main members of the structure.

Probably the greatest saving in the cost of attachments for wood poles for single-track construction is to be secured by the use of the angular tee-bracket which, due to its extension below the messenger cable, offers an ideal means for fastening pulloffs and steadies. The tee section provides an excellent section for cast clamp attachments and the sloping leg provides a means for adjustment, both horizontally and vertically. This method of attachment eliminates direct attachment to the pole itself. Therefore, another saving in labor is secured.

Recent electrifications employing concrete poles have been built entirely with pole clamp attachments rather than with multiple holes in the poles for fastening spans, pulloffs, steadies, and guys. Multiple holes are required for adjustment. Cast or forged pole attachments for concrete poles save construction cost because they permit quick adjustment.

Distribution of Materials

Probably one of the easiest ways to waste money on a construction job is to neglect the planning of material distribution. The cost of distributing and warehousing can run as high as 5 per cent of the total cost of construction, or it can run as low as $1^1/_2$ per cent. The difference in these two extremes is the penalty paid for inadequate planning. The higher cost may come from rehandling too many times, or it may be attributable to storage costs. It is to be remembered that material on the job is an investment, and as such, commands an interest rate, the same as any investment.

The careful planning of material distribution involves a knowledge of all materials necessary for the installation, as well as of the location and use of the materials. As an example, a completed structure may be equipped with such attachments as insulators, messenger clamps, pulloffs, or steadies depending on the purpose of the structure. At the proper time all of these materials must be gathered together and distributed for each structure, preferably when the supply train or truck is delivering other material to the same location. If the fittings and insulators are conveniently crated for handling, the work becomes easier and faster and the cost of distribution is less.

For a wood pole installation most of this equipment can be delivered with the pole, itself. To illustrate, when the pole is unloaded from a work train or truck, other equipment and attachments can be unloaded simultaneously. This procedure saves time, assures the delivery of the required materials to the proper location, and permits the linemen to assemble the wood structure without delay.

Field Prefabrication

With a knowledge of structure dimensions, there is every reason to prefabricate most spans before outside erection is started. Cable lengths for guys and spans can be cut, bundled, and tagged for

Figure 16. Simple catenary span with single contact wire on Canadian National Railways

2,400 volts direct current

Figure 17 (left). Simple catenary span with two contact wires on the Sorocabana Railway, Brazil

3,000 volts direct circuit

Figure 18. Simple catenary span with 50-foot subspans on ore haulage tracks of the Phelps-Dodge Corporation, Morenci, Ariz.

the various structures. Cable sockets, certain clamps, and insulators will be included in these assemblies. With this procedure cable assemblies can be distributed with the hardware and fittings, or even when the structure is distributed. For wood pole lines, it is easily conceivable that a completely assembled structure may be delivered by truck, the assembly having been made at some nearby location where a material center has been established. It is particularly true for the single-track structure.

There is one important procedure to keep in mind with respect to the labor costs on structure assemblies. Failure to recognize this procedure is apt to cause high construction costs. The procedure is

1. Fabricate the structure as far as possible in the plant of the manufacturer.
2. Make all attachments and perform as much of the remaining assembly work as possible on the ground before erection.
3. Keep the work train time to a minimum. It cannot always appear when it is wanted.

Insulators

The erection of insulators may not be possible at the time the structure is set, but if we take a lesson from the transmission line builder we will discover that many transmission towers for high voltage lines, carrying a long string of insulators, are erected with all hardware and insulators in place. It may be necessary to protect the insulator during the erection of the tower but this is an incidental cost compared to that of climbing the tower, pulling up the insulator string, and attaching to the crossarm. Likewise, if the insulator string can be assembled on a wood pole structure prior to setting, and the entire assembly raised at one time, it is possible to save the time of climbing the pole to erect the insulators.

Catenary Spans

For a number of years the trend in catenary span design has been towards an intermediate cable suspended from the main messenger cable. The contact wires are supported from the intermediate cable. Recent observations on several installations apparently indicate that some of the simpler catenary designs are preferable and, due to less complexity, are cheaper and easier to erect. We refer to the simple catenary span with the single large contact wire, also the modified simple catenary span supporting equilength subspans.

As examples, we refer to the recent progression of electrifications on the lines of the Sorocabana Railway in Brazil and the main line electrification of the Utah operations of the Kennecott Corporation in Utah. Both are 3,000-volt d-c electrifications. The Sorocabana installation uses the simple catenary span with two contact wires for high currents. The intermediate wire was omitted. This is not a new idea, having been employed on the first Milwaukee electrification. The Kennecott main-line installation with 250-ton locomotives was built with the subspan, utilizing a main messenger cable, an intermediate cable, and one contact wire. However, this system differs radically from the usual compound type of catenary span in that the intermediate wire actually serves as a main messenger cable for short 50-foot catenary spans. The intermediate wire is supported at 50-foot intervals by the main messenger cable, thus requiring three main hangers on a 150-foot span. The 50-foot subspan in turn supports the single contact wire with three short hangers or clips, two of which are of the same length. The contact wire is 350,000 circular mils and performs the same function as two contact

Figure 19. Simple catenary with 400,000 circular mil contact wire on haulage way Pittsburgh Consolidated Coal Company

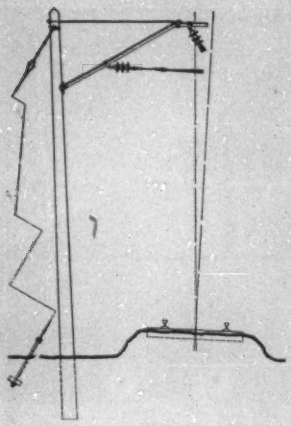

Figure 20 (left). Rodpulloff assembly attached to T-bar bracket; Chilean State Railways

Figure 21. Rod pulloff assembly aligning two contact wires; Sorocabana Railway

wires on many other electrifications. Incidentally, higher currents than those encountered at Utah are being collected on a simple catenary system of the Pittsburgh Consolidation Coal Company near Pittsburgh, Pa., where the voltage is considerably lower. It is recognized that these simple catenary spans and single contact wire spans are in use on the Reading, the Great Northern, the Pennsylvania, and the Milwaukee, and these railroads are not operating at slow speeds.

The amount of current collected by the pantograph has considerable bearing on the design of the catenary span. A simple span constructed with loop hangers and with large copper equivalent in the messenger cable as compared to the contact wire, may cause burning of the messenger cable and hanger loop because of high current through the hanger. Heretofore, it has been the practice to select a compound catenary span for the higher currents. This span makes use of clamped joints on all wires and cables, thus eliminating the possibility of burning due to hanger movement. However, currents of greater magnitude than formerly permitted can be carried through a simple catenary span if the ratio of conductivity of the main messenger to contact wire is reduced. This is accomplished by using a larger contact wire and at the same time reducing, somewhat, the conductivity of the messenger cable. This has been done on a number of installations. Furthermore, greater current can be permitted through the flexible catenary hanger if the messenger cable is properly protected with a sleeve or shim.

Naturally, the simple catenary span has greater application on the higher voltage lines where less conductivity is required. We refer to the 11-kv lines. Where these higher voltages are used the messenger cable may be a high-strength composite cable, the chief function of which is to support the contact wire. Naturally on long spans there is a lower limit to the size of the main messenger cable or, we might say, to the weight per foot of the system. Conductivity ceases to be a factor in this weight problem and a minimum weight per foot is finally reached on the basis of strength and structural requirements.

With respect to high speeds it is believed that a more rigid catenary system is possible providing the collector has sufficient sensitivity. This problem is similar to the track problem. Some years ago a resilient track was necessary for the rigid truck and spring suspension on freight and passenger cars, while today, particularly with passenger cars, the spring suspension is such that a more rigid track is possible, and these cars are certainly operating at higher speeds than the older types. Therefore the large contact wire which is considerably more rigid than the 3/0 and 4/0 types can be used for high speeds, and the heavier contact wire has a tendency to "weight" the system and retard the usual wave in the contact wire preceding the collector.

We must not forget when we are discussing simple catenary that there are systems such as the Chicago, North Shore and Milwaukee Railroad which operate at speeds in the vicinity of 80 to 90 miles per hour with direct suspension, and the collection of current is considered to be satisfactory. This does not mean that the simple catenary span, which has better characteristics than a direct suspension span, is possible for high speeds.

The usual practice is to erect catenary spans with a work train. A considerable saving is possible by limiting the work train to the stringing of messengers and cables. Other span work can be accomplished without the work train.

There is nothing to be gained by stringing messenger cables and other wires from trucks or from other off-track equipment. So far, the work train is preferable. However, there are many operations that, heretofore have been accomplished with the work train that can be handled in another manner. We refer particularly to the attachment of the catenary hangers, messenger sleeves, and duplex clips. Experience has shown that these can be erected by a 2-man crew, one man sitting in a boatswain's chair while the

Figure 22. Two-track steady; Sorocabana Railway

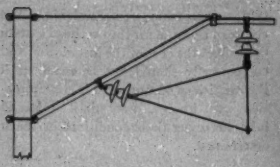

Figure 23. Single-track triangular steady—bent T-bar bracket provides adjustability; Chilean State Railways

other man is a ground attendant. As many 2-men crews as necessary can be scattered over a system for the purpose of maintaining the construction schedule. The resulting cost of erection is below that with the work train.

Pulloffs

There have been many revisions in pulloff design, most of which have simplified the device. Several recent installations have been built with simple rod pulloffs that are light in weight and comparatively cheap. Some of these installations have required the 3-rod pulloff for the alignment of an intermediate cable and two contact wires, while others have required the 2-rod pulloff for simple catenary with two contact wires. There is also a third type that has been installed on the subspan catenary which requires one rod attachment to the intermediate cable and one to the contact wire. All of these pulloffs are built with straight rods converging on a pulloff casting where rod length adjustments can be secured.

For single-track bracket construction the pulloff is attached to the insulator string which in turn is attached to the T-bar bracket. The insulator attachment to the T-bar bracket is adjustable along the axis of the T-bar, thus providing a quick means for trolley wire alignment. Simplicity in design of both the pulloff and the attachment will reduce construction costs.

Steadies

For this discussion two types of steadies will be described, namely, a 2-track steady for cross span or portal structures and the single-track steady for a bracket arm.

The 2-track steady which has been installed on many miles of the Sorocabana Railway in Brazil is not attached to the supporting structure. The steady assembly is constructed just by carrying a cable span between the two main messenger cables. Insulation is placed in the center of this span. On either side of the insulation, a steady rod is carried to the

Figure 24. High work train cost is usually due to lack of track availability

contact wire or to the intermediate cable. It will be noted from Figure 22 that with this "X" construction the contact wires, although permitted to move a slight amount, cannot swing to a dangerous position. Usually with this design it is necessary to erect the supporting string of suspension insulators at an angle, as illustrated, thus providing greater stabilization by creating a higher mechanical load in the steady span between the two messenger cables.

A steady span for single track can be built with a triangle of rods, two corners of which are attached to insulator strings

Figure 25. There does not seem to be any substitution for the work train for erecting steel structures

and the third corner to the intermediate cable or to the contact wire. The upper corner of the triangle is attached to the supporting insulator string, while the outer corner is attached to another insulator string which in turn is supported by the T-bar bracket. Analysis of this arrangement will show that there can be no movement to the right since the lower rod of the triangle and its insulator string become a pulloff. If the wind is from the right, there is a tendency for the lower section of the triangle to travel to the left and at the same time for the left-hand corner of the triangle to elevate. With this action there is some movement of the trolley wire to the left but the movement cannot become great since left-hand movement of the trolley wire also tends to raise the trolley thus retarding the sway. This type of steady is in use on the Sorocabana Railway, the Chilean State Railways, and the main line 3,000-volt d-c electrification of Kennecott Copper Corporation in Utah.

Rail Bonds

For heavy currents where it is necessary to use a bond of 4/0 capacity or greater there seems to be no cheaper bond than the short U-shaped bond welded to the rail. Although there are several installations of arc weld bonds on systems where the trolley voltage is above 600, it is general practice to use a gas weld bond for electrifications with higher voltage. It is usually cheaper to install the gas weld bond where higher voltages prevail.

For the a-c electrifications, such as 11 kv, American practice has proved that the light head-of-the-rail signal bond is ample. The Pennsylvania Railroad has installed many head-of-rail bonds that do not require welding. The cost of bonding for this type of installation is approximately half the cost of the equivalent welded bond.

Basic Materials

Since the choice of materials readily can affect cost, a short discussion on their selection will indicate where these savings are possible. The major tonnage of material is to be found in the structures. Structure material is determined partially by geographic location, track conditions, traffic density, number of tracks, and expected life of the structure.

Geographically there are many reasons for the omission of wood in certain parts of South America. The tropical climate may be responsible for decay in some locations, while in other locations, termites or other wood destroyers prevent

Figure 26. By reducing the work train to a miniature, work train costs also are reduced; Cleveland Union Terminals

its use. Concrete or steel is the answer. Along coastal sections steel structures must be well protected from salt, otherwise wood or concrete may be necessary. Concrete structures, like steel structures, also have a maintenance problem due to the corrosion of reinforcing bars.

When it is said track conditions affect the choice of structure, reference is made to the condition of the soil. It may be uneconomical to install a long-life structure due to slides or to the shifting of materials in cuts and fills. This condition is found on newly built tracks and on the large open-pit mine electrifications.

Traffic density influences the choice of structure. More trains mean more electrical load, and more electrical load means heavier structures. It is difficult to beat the first cost of a wood structure for a single track railroad, but the "density of traffic" factor may throw the choice into steel structures. This can be determined only by an economic study.

The number of main line tracks is based on traffic density, therefore, what has just been said about traffic density applies equally well to the choice of material for more than one track. Steel is usually favored for multiple track construction, if for no other reason than appearance, which, incidentally, is a real factor in electrification.

The expected life of a structure must be based on the life of the electrification. A short-time electrification may be constructed with wood since replacement may never occur. On converse, our big carriers would not consider short-life material if replacement is to be anticipated many times during the life of the electrification. Our western roads have found wood structures to be economical because maintenance and replacement costs have been comparatively low. The reverse is true in the east.

The choice of messenger cables is also greatly dependent on the amount of electric energy transmitted over the catenary system. If the amount of energy is high the copper equivalent in the distribution system will be high. If the amount of electric energy is low, the conductivity of the system is low. For a high current system, the cables are copper or a composite of nonferrous materials. Likewise fittings such as hangers and clamps are made of high conductivity or nonferrous material because they are current carrying. For a low current system a nonferrous messenger cable may not be necessary since the contact wire may have sufficient conductivity adequately to handle the electric load. This is also true of hangers and clips. They will not carry much current. Therefore, from a current-carrying standpoint, the choice of material, ferrous or nonferrous, in the overhead system is partially controlled by the amount of electric energy to be carried.

Corrosive atmospheres also affect the choice of materials. There are many overhead systems built for relatively small current that could have been constructed with iron fittings, properly protected, rather than with nonferrous materials. Many systems have been built with ferrous materials and after 20 years of operation are still in good condition. Reference is made to such installations as portions of the Milwaukee Railroad and Canadian National electrifications, also to many of the large industrial operations in the western United States and in several foreign countries. Malleable iron fittings properly galvanized, have an exceptionally long life even in the presence of certain atmospheric contamination. The choice of material for the overhead system does affect the cost of the installation and consequently should not be made until the conditions of atmospheric contamination are known, and until material behavior under these conditions has been determined.

Work Train Cost

One of the largest items of cost for the overhead distribution system is chargeable to the work train. The high cost is not necessarily due to train and crew alone, but to lost time caused by the lack of track availability. There are examples of track availability of only a few hours, in fact there are locations, due to heavy traffic density, where availability is less than 2 hours in a 24-hour period. With this picture of work train cost it certainly can be concluded that either there must be greater track availability or there must be less work train operation.

It is quite difficult to increase track availability, particularly on the 1- and 2-track railroads carrying a large volume of traffic. It is, however, possible to increase track availability by bunching freight trains or by planning certain train delays, but the net gain is not big and will not account for an appreciable reduction in costs. The greatest savings is to be attained by eliminating as much work train movement as possible, by substituting off-track equipment, and by using

Figure 27. Push-car used on recent electrification of the Sorocabana Railway, Brazil

additional ground crews. There does not seem to be any substitution for the work train for erecting steel structures or for stringing cables, but wood structures may be erected with off-track equipment or even with ground crews, and such devices as hangers, clamps, and feed taps can be erected with the aid of a boatswain's chair.

Another means of reducing cost is to reduce the size of the work train, in fact reduce the work train until it becomes a miniature. Some of the electrifications back in the 30's took advantage of the small gasoline car, also the ordinary push-car used for track work. The Reading and the Cleveland Union Terminals used small self-propelled cars and found them to be quite economical and satisfactory. Small gasoline-driven cars were used on such recent installations as the Morenci operations of the Phelps Dodge Corporation for almost all types of distribution work with the exception of cable stringing, and even this was attempted but ultimately abandoned because the car was too light to hold the stringing loads by mere track adhesion. Although this light equipment must be treated the same as a work train, in that it must be controlled by the same orders and clearances, it is by far the most flexible of the two types of work train equipment since it can be removed from the track and rerailed at points other than standard track turnouts.

The small self-propelled cars can be housed on the job. It is not necessary to return them to a terminal for fuel or overhaul. They are a part of the tool equipment, and as such, represent an appreciable reduction in work train cost.

Clearances

The second main subject pertinent to cost reduction is "clearances.' The word, itself, seems harmless enough, but to the distribution man it spells cost. Probably an example will clarify this statement.

At the present time the Kennecott Copper Corporation is building a main-line 3,000-volt electrification between the Bingham Mines in Utah and the mills at Arthur and Magna. The catenary system is being built over new track; in fact, the entire roadbed is new. Here is an installation that is not hampered by existing steam facilities, low highway structures, yard restrictions, or any other limitation that becomes the nightmare of the designer when planning overhead for an existing steam railroad. In this particular case the designer could proceed with the minimum of lost motion, could produce standard designs for application on similar stretches of track, could call for highway structures with the proper overhead clearances, could disregard telegraph and communication lines; in fact, actually could produce the finished line on paper, knowing that the system would be built as planned. If the designer of this system were to move to one of our big existing railroads and attempt to design along the same lines he would be greatly hampered by running into innumerable problems unheard of on the Utah installation. For instance, we will take the subject of contact wire height. Every operating man will demand a trolley wire height of at least 22 feet but on an old railroad this specification must be violated many times. On the Utah installation the desired height of trolley wire has been maintained at all locations. Bridges and other limiting structures have been built to secure this height.

Figure 28. Gasoline-driven car used for overhead construction on the Morenci operations of Phelps-Dodge Corporation

On the existing railroads, many bridges and signal structures have been erected for a period of years. The crowding of overhead wires in these restricted locations becomes quite a feat. Although the operating department of a railroad usually demands a high wire in all locations there are many exceptions caused by bridges, tunnels and other restrictions where physical limitations existed prior to the electrification study. In some respects this practice seems redundant. It is not good practice to permit trainmen on the top of trains where high voltage wires are suspended above their heads, except, of course, in an emergency. Therefore the lowering of the overhead system to permit a contact wire height of 20 feet, except at highway crossings, cannot be too serious and a decided saving in structure costs is possible. The contact wire height on the catenary system of the Cleveland Union Terminals is 19 feet.

Figure 29. Offset catenary on haulage of Columbia Chemical Company, Ohio

One method proposed for the maintenance of a high overhead clearance and at the same time reduce costs, is to offset the catenary system from the center line of track. This scheme of "have your cake and eat it too" provides steel structure clearance over the track of 22 or 24 feet

Figure 30. It is not necessary for the railroad to detail completely the supporting structures

Figure 31. Ground crews save work-train cost

since the catenary system is offset to the right or left. The contact wire is lowered to an elevation of 19 or 20 feet. Although this method of construction may require a redesign of the collection equipment, the anticipated savings seem to be sufficient to warrant the cost of development of a new collector. For single- or double-track installations it may be possible to secure attractive savings in structure costs due to the lower positions of the mechanical loads.

Psychological Barriers

The third main subject relative to cost reduction is "psychological barriers."

All engineers work against a certain barrier when starting a large project. The effect of the barrier usually is evidenced in the planning of the engineering work and represents to a certain extent the "unfamiliar" phase of design. As an example, all installations are based on a cost estimate. If the construction crews meet the estimate, the job is considered to be economical but if the cost of construction is greater than the estimate, there is bound to be some doubt as to the economic outcome of the job. As a result, the planning or engineering department is apt to "play safe" wherever possible. One place where the engineering departments for steam road electrifications have been playing safe is in steel design.

It is the task of the railway's engineering department to select the general design of structure and to locate the structure advantageously along the right of way. If economics are to be considered it is not the task of the engineering department to design the structure. Many electrifications have cost too much because of too much engineering. In the case of the steel structure, many railroad engineering departments have detailed completely every column, every truss, every knee brace, and then, after submitting the designs to the fabricator, the fabricator has redesigned every structure. This duplication of work is costly. The real answer is, "let the fabricator detail the steel, he will do it anyway." This procedure is also logical for other classes of design including transmission systems and catenary systems. After the general schemes have been selected, there is no reason why the burden of transmission and catenary design cannot be boiled down to a "system design," then let the manufacturer design the fittings.

It has been estimated that as much as three per cent of the total cost of an installation can be saved by eliminating duplication in design. There is no need to carry the structure design beyond the space diagram, and there is no need to carry the catenary design beyond the general scheme. Further attempt at design in both cases is simply duplication and is very costly.

Future Considerations

This paper has emphasized certain means for the reduction of costs of catenary distribution systems. Let us review a few of these means, also add a few suggestions that, today, may not be quite as evident as the ones discussed.

1. The shallow footing for structures suggests a means of saving cost by permitting the use of mechanical digging equipment. The hole for the footing must be large; therefore a large clam shell or similar type of bucket is possible. This equipment may be mounted on a tractor, thus removing the track availability factor.

2. A centralized concrete plant offers certain savings over the concrete train as evidenced by its use on many expressway installations. Many building contractors use ready mixed concrete because it is cheaper. Our city transit systems use ready mixed concrete for the setting of thousands of steel poles for trolley coach overhead construction. They have found it to be cheaper than concrete mixed by other methods.

3. Off-track equipment saves work train cost. Off-track equipment consists of material trucks, concrete-handling trucks, cranes for the erecting of some steel structures and wood poles, tractors for certain wire stringing, and other smaller equipment.

4. Some off-track equipment may be of the rerail type that will permit the vehicle to operate on either rail or ground.

5. Many overhead construction operations such as wood pole setting can be accomplished with a ground crew, thus saving work train cost. Only the distribution of wood poles in areas which are inaccessible by truck need be accomplished by work train. Ground crews can erect catenary hangers, duplex clips, feeder taps, and many other fittings that would require work train time.

6. Small gasoline-driven cars, also small cars with staging, drawn by the gasoline-propelled car, can be employed for many construction operations.

7. It is possible that a wider pantograph may reduce costs by permitting greater offset of the contact system at points of pulloff. Again, a different design of collector somewhat similar to the pole trolley may provide a means of cheapening the overhead system. The pole-type collector with swivelling movement also will permit an increase in offset for the trolley wire on curves, thus permitting a decided reduction in construction costs.

8. If a new type of collector is available, as an example, the pole type, structure costs can be reduced by reducing the height of the steel. If the operating department of the railroad requires a 22-foot clearance between track and overhead system, it is possible with the aid of a different type of collector to offset the catenary span some 4 or 5 feet from the center-of-track and erect the steel truss to just clear the 22-foot requirement. The catenary span would be located to the side of the center of the track and, of course, below the steel truss. Due to the lower position of the catenary span, its side loads on the structure would be reduced.

9. Higher contact wire voltages reduce the amount of copper in the overhead system. This reduction, in itself, is quite significant and represents a big saving; however, another appreciable saving probably will be found in the cost of the structure. The cost of a structure is somewhat in proportion to the mechanical loads applied to the structure. Savings in steel structure cost and conductor cost more than offset the increased cost of insulation.

10. Head-of-rail bonding with the small pintype signal bonds for high-voltage a-c

Table II

	Possible Reduction, Per Cent	
	Steel	Wood
Central concrete plant	0.25	
Off-track—crane		0.50
Off-track—small motor cars	0.25	0.50
Clamped attachments		0.50
Distribution of materials—by truck		0.25
Ground crews—cross span	0.60	0.80
Ground crews—catenary spans	1.00	1.20
Simple catenary span	1.80	1.80
A-c bonding	1.10	1.10
Offset catenary or lower contact wire	7.50	1.50
Engineering	2.00	3.00
Totals	14.50	11.15

systems will save about half the cost of ordinary bonding where the currents are low. This is not true for d-c installations.

11. Engineering costs may be appreciably decreased by giving the fabricator of structures and other materials the design work he naturally will duplicate.

Anticipated Cost Reduction

The title of this paper indicates that costs may be the stumbling block for electrification, and the opening sentence indicates that there may be possible cost reductions. It is hoped this paper has called attention to such cost reductions, but a summary of possible savings based on the suggestion of this paper may further emphasize cost reduction. Here are some figures taken from the same three estimates used at the beginning of the paper. The per cent reductions are based on the total cost of electrification as shown in Table II.

Probably there may be some criticism of these cost reduction methods, but they are based on observation, experience, and planning. If these suggestions, when put into practice, reduce the cost of electrification, it will make the Diesel and electric systems, which are both good methods of transportation, less competitive with regard to cost.

Discussion

K. H. Gordon and L. B. Curtis (Pennsylvania Railroad, Philadelphia, Pa.): This paper is a significant contribution to the literature on the economics of railroad electrification. Since a change from steam to electric operation cannot be made in relatively small units as can a change from steam to another form of self-propelled locomotive such as the Diesel-electric, and since the overhead distribution costs are a very substantial portion of the total cost of electrification, any measures which can be taken to reduce these costs are important. However, reduced construction costs never should be obtained at the cost of excessive maintenance.

While this paper is concerned primarily with single-track and 2-track electrification, a number of points are made on which, we believe, experience with an extensive multitrack electrification will have some bearing.

One of these points is referred to as "psychological barriers." It is our belief that, with an engineering organization experienced in electrification design, the engineering costs are reduced to a minimum consistent with safe and adequate construction, and while there is a natural human tendency to do things the way they have always been done, our observation has been that the experienced engineer is ever on the alert for new and better methods.

Many of the methods suggested by Birch are valuable in any electrification project, such as the maximum economical use of off-track equipment, the avoidance of duplication of effort in steel design, the use of small rail-head bonds, and, to a limited extent, the use of ready mixed concrete from a central mixing plant. The latter is chiefly of value, however in built up areas where access to foundation locations by truck is not difficult. In other locations the concrete train, a portable central mixing plant, has proved more desirable from a cost standpoint.

However, a number of the suggestions are questioned for use in an extensive multitrack electrification. Some of these are

Shallow foundations. Experience has shown that, except for certain difficult soil conditions, a deep "can-type" foundation requiring a minimum of forms and reinforcing is less costly than the shallow "gravity type" suggested.

Simple catenary. This type of construction is lowest in cost and is satisfactory for almost any electrification, and tests have indicated that the stresses set up in the wires are lower than with compound catenary. However, it is not considered necessary to reduce the conductivity of the messenger. In fact, in many cases it is desirable to increase this conductivity and utilize one or more jumpers in each span, firmly clamped to both the messenger and contact wire, to shunt the flexible hangers.

The use of light weight construction cars. It has been found that this type of car cannot be depended upon to shunt the track circuit; hence, it should not be used where the operation of trains depends upon the proper signal indication which is supposed to result from shunting the track circuit when the particular section of track is occupied.

Reduced height of contact wire. Electrification should not impose unnecessary restrictions on the operation, in locations where it is necessary or desirable to have men on top of cars with steam operation it may be just as much so with electric operation, and each such location should be studied to balance the desirability of a lower contact wire against that of continuing existing practices. The suggestion that contact wires be offset and lowered does not seem practicable for a multitrack electrification, as it would appear to restrict the direction of operation of locomotives and would greatly complicate construction at interlockings.

Pole-type collectors. These are not considered practicable for high-speed operation.

Duplication of engineering. While we largely agree with Birch's comments with respect to steel design, little such duplication has been found in the catenary system itself.

We believe the following suggestions would also reduce the over-all cost of overhead distribution when it is practicable to employ them:

1. Give engineering a sufficient start on construction so that completed final drawings may be used and sufficient material may be on hand to avoid delays.

2. Complete all construction work before energizing the system, thereby eliminating the need for working under clearances.

H. F. Brown (New York, New Haven and Hartford Railroad, New Haven, Conn.): This paper represents the thoughts of one who has been closely associated with much of the existing catenary design in both North and South America, and for that reason is a valuable addition to the existing bibliography on this subject.

While it is true that a number of existing steam roads were electrified during the first three decades of the twentieth century for urgent economic or operating reasons, to say that electrification is "progressing at a snail's pace" is hardly true in the broad sense, for the universal application of the Diesel-electric locomotive is primarily because it is an electric locomotive which carries its own power plant along with each unit. It is just as much an electric locomotive as one with pantographs or third rail shoes—and so successful because of its electrical characteristics. The railroads of the United States *are* electrifying in this sense at a very rapid rate—and the field for the manufacturer of such electric railway apparatus is a very large one today.

Birch points out a very significant fact first of all, that the cost of the overhead contact system is not greatly different from the incremental cost of the Diesel-electric over the straight electric locomotive. This never should be lost sight of by the economists studying the substitution of Diesels for steam, especially if a part of the railroad should be already electrified.

The author next points out ways and means where, in the light of past experiments and experience, such costs *may* be reduced appreciably, one of the methods being to do just as much of the construction work by means which do not interfere with use of tracks for revenue business. This should be extended to line maintenance work, after electrification.

Many of the larger installations of overhead contact systems in the past have been over multitrack railroads with very dense traffic, hence construction costs have been high. It should be, theoretically, quite a bit cheaper per track—per mile—to install overhead construction over a single-track or a double-track line than over a 3- or 4-track line, because much of the construction can be done from the wayside on the former, whereas the construction over the middle tracks always must be done largely from those tracks themselves.

Although there has been helpful design for certain hardware used in overhead catenary construction from the various manufacturers specializing in such hardware and parts used for direct suspension for street railway and, more recently, trolley coach-contact systems, nearly every railroad that has electrified has found it expedient to de-

sign much of its own hardware because of lack of existing standards and other considerations which the railways thought they needed—and with which the manufacturers often could not agree. High stress insulators are just one example. There is a greater amount of agreement and design which could be standardized today than formerly.

There are often other considerations in contact system design besides ample current conductivity and mechanical strength. For example, a line must be not only capable of withstanding heavy sleet or ice loads, but also must withstand the possible mechanical or electrical methods used to remove it. Spans in locations exposed to high wind velocities must be short enough so that horizontal displacements cannot cause pantograph trouble.

The author emphasizes another fact concerning which too little study has been made in the past—the closer co-ordination of pantograph design with the overhead design.

Although there is a decided trend today toward the increased use of Diesel-electric locomotives, there are many economic factors and adverse possibilities in the foreseeable future which easily can change the economic advantage of this type of motive power—and when this time comes the railroads are still going to demand the characteristics of this type of power, namely, electric locomotives, and Birch's paper will be found useful in those days even if there is very little construction of this type during the next four or five years.

Sidney Withington (New York, New Haven and Hartford Railroad, New Haven, Conn): Birch refers to relative progress in electrification in the United States as compared to progress in all other parts of the world. The answer lies mainly, as he points out, in scarce and expensive fuel elsewhere. We always have been a wasteful nation because we have had so much in the way of natural resources. Just now, as Birch has also pointed out, the Diesel engine is having a notable vogue among American railroad men. What the future will bring forth in the petroleum industry and in the gas turbine which may be developed to burn low grade residual oil or powdered coal will be of considerable interest.

Birch gives figures of about 27 per cent as representing the ratio of the cost of the power distribution system in an electrification project to the total cost of the entire project. It is interesting to note that in the New Haven electrification, installed at various periods during the past 40 years, the distribution system accounts for 26.7 per cent of the total capitalization as of the present time, including motive power, power plants, and substations.

It is perhaps a little unfair to blame the structural engineer of the "railroad's bridge department" for excessively heavy catenary supporting structures. It is true that the duty of the catenary supports is a very different type of duty as compared with that of railroad bridges, but it is also true that the power transmission and distribution facilities installed directly over busy railroad tracks where heavy trains are operated at high speeds present many problems to the railroad designing engineers which the designers of power transmission lines do not meet. A higher mechanical factor of safety for the railroad facilities is very necessary as compared with ordinary transmission lines. This applies to steel structures, wood poles, catenary cables, and strands and hardware.

As Birch has pointed out, wood poles offer some very intriguing advantages, where they are available without too much transportation cost. It should be borne in mind, however, that even with proper treatment, the life of a wood pole is not unlimited, and general replacement of wood poles in a busy electrified section of railroad may be a major headache for those responsible for continuous operation of the railroad.

The author mentions the advantage of rolled H sections as compared with lattice construction. This is an important consideration even apart from first costs. Painting the structure must be done periodically regardless of the design, and painting a light lattice or laced structure is a very expensive process as compared with a solid member. Some railroads, incidentally, have used rolled H beam design for four tracks as well as for 2- and 3-track sections.

Off-track material assembly and erection has such obvious advantages, especially with modern mobile equipment, that it should be seriously considered by all responsible. It is, however, true that on many railroads, especially those in congested territory where rights of way are restricted or in rough terrain where there are numerous cuts and fills, the use of such equipment is likely to be limited.

Birch points out the advantages of portal structures which are self-supporting so far as loads across the tracks are concerned, even though they may require somewhat heavier steel. It may be said in this connection that in designing the foundations for such structures there must be some allowance made for possible loads *along* tracks on account of broken lines or other abnormal conditions, and also that, especially where deep frost may be experienced, the foundation must be carried down below the frost line if settlement is not to cause trouble, especially on fills.

The author touches briefly on catenary design. A large book might be written on this phase of the subject, and even then would be likely to cover but a small part of the problems which are met. The flexibility of the contact system is tied closely to the pantograph design. A very important factor often overlooked is wind loads and horizontal stability, epecially with long spans and at exposed locations. The use of noncorrodible materials has been of increasingly greater interest on existing electrification, as deterioration of carbon steel members has been experienced with the passage of time, and expensive replacements or frequent painting have been found necessary. Noncorrodible material is expensive in first cost but may represent attractive savings in the long run.

Birch mentions the use of small gasoline-driven construction cars for erection on busy tracks. One important limitation in such equipment which must be kept in mind is that it may not be heavy enough to operate track signal circuits, so special protective methods must be used to avoid trouble.

Adequate clearances over track for the catenary are important, even though they must be reduced at overhead highway bridges, and even though it may be practicable to prohibit men working on tops of cars. A low trolley may increase the difficulty in cleaning wrecks or damaged pantographs on motive power. The offsetting of the contact wire from the center of the track, which Birch suggests, is of little advantage so far as permitting men safely on tops of cars is concerned. If a man *can* reach the wire from the car he will be sure some time to do so.

This paper is a valuable contribution to the literature of catenary design, and the Institute is to be congratulated in having obtained the data which he has presented.

L. W. Birch: It is always gratifying to receive discussion of one's paper, particularly if the participants in the discussion have considered the subject worthy of a written discussion. These discussions uncover other's thoughts—new angles, and may even be controversial to the extent of offering quite different solutions. The author appreciates the additions that Gordon and Curtis of the Pennsylvania Railroad and Withington and Brown of the New Haven Railroad have made to his paper. In an effort to answer the questions arising from the discussions, provided by these four discussers, the following brief explanation is presented on the most important subjects.

(K. V. Gordon and L. B. Curtis) 1. With respect to the reduction of engineering costs, the paper deals particularly with the duplication of costs for steel design. Frequently, the railroad will detail all structural pieces, a job which the structural company will later redesign. This duplication is costly, and is paid for the second time to the structural company.

2. Reference is not made to gravity footings in the paper. The author particularly described slab footings similar to those used on the Lackawanna Railroad.

3. When the statement was made that the use of a large contact wire would permit the decrease in the conductivity of the messenger cable, it was not meant that the total circular-mil capacity of the system should be reduced.

4. It is true that lightweight construction cars may not operate signals. However, this does not necessarily eliminate the use of the equipment. There are many tracks where lightweight equipment can be used without any particular effect to train operation.

5. There is no particular complication to the use of offset catenary. In fact, it is in use on a number of industrial electrifications. Offset catenary will permit the lowering of the system, and as a consequence, a reduction in cost can be secured, due to the lesser moments encountered in the steel.

6. The pole-type collector referred to in the paper does not include the conventional street-car type. Pole-type was merely a term used to describe a wiping type of collector, not a pantograph type. It might even include the bow type. Incidentally, a conventional pole collector with shoe is operating in and out of Chicago on trains running 90 miles per hour.

(H. F. Brown) 1. With respect to standardization of hardware and insulation, it is agreed that there have been many designs which duplicate earlier work. However, of later years, most steam railroads have

recognized the benefits of standardization, and as a result there is not so much duplication. As an example, such early electrifications as the Illinois Central and one section of the Canadian National used $7^1/_4$-inch suspension units for 3,000 volts direct current. During the same period, the Cleveland Union Terminals and the Lackawanna used 10-inch suspension units for the same voltage. During the past eight years, the 10-inch unit has been selected for practically all 3,000-volt d-c electrifications—example, the Canadian National Railways and the Chilean State Railways. All 11-kv systems have used the 10-inch suspension unit.

With respect to hardware, there has been some attempt to standardize such items as the catenary hanger. As an example, the Illinois Central, Cleveland Union Terminals, Chicago South Shore and South Bend, and the Canadian National Railways have used one type of hanger over a period of 22 years, new installations having occurred intermittently throughout this period.

2. High strength suspension units employed in dead-end work have been standardized during the past 10 years, with the result that the 25,000-pound unit could be considered a standard. The 15,000-pound 10-inch suspension unit has been used almost universally for tangent construction.

3. It is agreed, absolutely, that the collector designer and the overhead designer should co-ordinate their work at all times. Incidentally, mention was made of a pole-type collector earlier in this rebuttal. The development of this collector must be conducted by the collector designer, and the overhead engineer must co-ordinate his efforts with the collector designer.

(Sidney Withington) 1. There would be little point in recommending wood poles for construction in the eastern section of the United States; however, the West and the South can erect wood poles economically and expect a fair life from their local timber. Particularly in the West, the cedars can be erected and maintained for single-track construction cheaper than most steel. This is true in the South, where the full-pressure-creosoted pine pole is obtainable without much freight cost.

2. Off-track equipment can be used on the greater portion of all railroads. Referring to one of the examples used while making the estimate for my paper, the western railroad crosses the Rocky Mountains, also a portion of the Colorado-Utah desert. It was found that more than 90 per cent of this railroad could be served with off-track equipment for the purpose of erecting poles, digging holes, and attaching hardware for electrification.

3. The Lackawanna footings are approximately four feet in the ground. They are slab footings, not necessarily gravity footings. It is apparent from recent inspection, that this depth has been satisfactory. It is agreed that the footing must be of sufficient depth to prevent frost interference.

4. A 19-foot contact wire scarcely can be considered a low wire when it is used over a great many miles of track and throughout the entire Cleveland Union Terminals electrification. There are many miles of "low trolley wire" on other roads, where it is necessary to reduce height because of bridges and other physical limitations.

Shunt Capacitor Installation for Single-Phase Railway Service

H. F. BROWN
FELLOW AIEE

R. L. WITZKE
MEMBER AIEE

A 10,000-KVA BANK of shunt capacitors recently placed in service on the 22 kv electric traction system of the New York, New Haven, and Hartford Railroad, is the first installation of this kind to be made on a 25-cycle single-phase system. Made up of 25,400 kva of 60-cycle 15-kva units, it is also one of the larger capacitor banks in operation. The installation is tuned by means of series reactors to control both the third and fifth harmonic voltages on the traction system. The units are automatically switched in steps of approximately 2,500 kva each. This reactive supply has increased the traction system voltage by as much as 20 per cent, and has released 6,000 kw in generating capacity during peak load conditions. In addition, it will save an estimated $20,000 a year in cost of energy losses by decreasing the hours of operation of several large frequency changers, previously operated much of the time as synchronous condensers for voltage control.

This paper reviews the conditions giving rise to the need for additional reactive capacity, compares the relative costs of synchronous condensers and shunt capacitors for 25-cycle single-phase service, and discusses the problems incident to the installation of capacitors on a railway system. The general characteristics of the capacitor station are given in some detail. Six months operating experience, and test results are reported.

The Railway Traction System

The initial electrification on the New York, New Haven, and Hartford Railroad, made in 1907 between New York City and Stamford, Conn., was operated at 11,000 volts 25-cycles single-phase, with power supplied by the railroad-owned station at Cos Cob. The present 3-wire system, Figure 1, was installed in 1912 just prior to the extension of the electrification to New Haven in 1913. The 3-wire power distribution system is supplied from the 11 kv, single phase generators at the supply points through 11/22-kv autotransformers, the midpoint of the transformers being connected to the rails. Balancing autotransformers are connected to the trolley and feeder circuits at suitable intervals along the 4-track electrified section, the spacing of the transformers being determined primarily by track switching requirements. The traction load is all on the 11 kv trolley-rail circuit, the transmission between the power sources and the balancing transformers being at 22 kv over the trolley-feeder circuits.

The present power supply consists of the railroad-owned station at Cos Cob, a tie at West Farms (East 174th Street, New York) to the Consolidated Edison Company Hell Gate Station; and four variable ratio frequency changer sets—one at East New York; one at Station *A*, the power station of the railroad-owned street railway subsidiary at New Haven; and two at Devon, Conn. for supply from the Connecticut Light and Power Company, Figure 2. Their capacities are indicated in Table I.

The system traction load has peak requirements as follows:

Maximum peak hour, 50,000 kw
Maximum peak 15 minutes, 57,000 kw
Maximum peak 5 minutes, 63,000 kw
Maximum swing, 72,000 kw

These loads usually occur between the hours of 5:30 p.m. and 6:30 p.m. on days of heavy weekend or holiday travel. The system power factor, measured at the sources, is between 70 and 73 per cent, which means that the reactive demands are approximately equal to the power demands. The system load factor is about 56 per cent. Occasional light load periods occur when the minimum hour is about 15,000 kw. A study of Table I indicates a deficiency in reactive capacity, particularly with one machine out of service at Cos Cob.

The ever-shifting nature of the railway load presents additional problems. The 22-kv transmission precludes the possibility of transferring the entire output of any one supply station to the extreme load centers on the system. The peak load center is well east of Cos Cob, approximately 60 per cent of the load being east of that station during the peak hour. The fact that the voltage at West

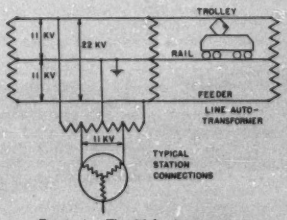

Figure 1. The 22-kv 3-wire system

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H. F. Brown is electric traction engineer with the New York, New Haven and Hartford Railroad, New Haven, Conn. and R. L. Witzke is central station engineer, industry engineering department of the Westinghouse Electric Corporation, East Pittsburgh, Pa.

This installation represents the co-operative efforts of the engineering staff of the railroad; of their consultants, Gibbs and Hill, Inc., who constructed the station; and of the manufacturer, Westinghouse Electric Corporation. The members of all these groups have contributed largely to the data in this paper. Particular mention must be made of the help of S. S. Watkins (F '46) and P. Goldstone (deceased) of Gibbs and Hill. A large measure of credit is due to engineers of the Southern New England Telephone Company, the American Telephone and Telegraph Company, and Bell Telephone Laboratories for their help in making the detail tests on the installation.

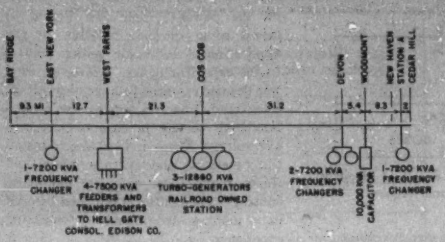


Figure 2. Location of power sources

Farms is not regulated is a further complicating factor for peak loads east of Cos Cob. All of these factors make it impossible to consider the total capacities in Table I when estimating the system loads that can be handled.

One of the most serious problems encountered was that of low voltage at the eastern end of the system during maximum load conditions. With the limitations of the supply sources at this end, the voltage during the peak hour, or with heavy concentrated local loads, would drop as low as 8,000 volts or 73 per cent, even with the voltage at Cos Cob raised to 11.8 kv. This imposed further limitations on the kilowatt capacity of Cos Cob because of the additional reactive that this station had to carry at the higher operating voltage.

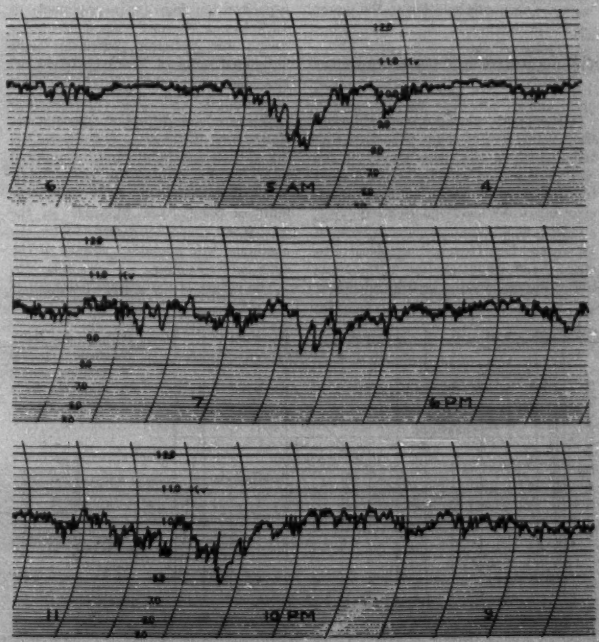
By 1942, the heavy traffic due to the war and the acquisition of heavier electric locomotives were clearly indicating the need for power system improvements. Studies were made on the network calculator to determine what could be done to improve voltage conditions with the heavier loads. These studies indicated that some further means were needed to sustain the voltage at the eastern end of the system, particularly by the addition of reactive supply nearer this load center, which would not only reduce transmission losses, but would allow a better use of the Cos Cob kilowatt capacity. These studies further showed that the addition of approximately 10,000 reactive kilovolt amperes at some point east of Bridgeport would provide the desired improvement.

Synchronous Condensers Versus Shunt Capacitors

The first reaction was to augment the existing reactive sources at the eastern end of the electrified section by additional synchronous capacity. However, the extensive use of shunt capacitors on 60-cycle 3-phase systems led to their consideration in this application. Studies[1] reported in 1943 show that when losses are capitalized at $150 per kilowatt shunt capacitors have economic advantages over synchronous condensers for stations up to at least 35,000-kva three-phase, at 13.8-kv and 60-cycles. As the capacitors available for 25-cycle service were standard 60-cycle units derated by a factor of 2.4, there was a general impression that capacitors could not be justified for 25-cycle service. However, synchronous-condenser capacity for single-phase 25-cycle service costs at least 2.25 times the corresponding capacity for a 60-cycle, 3-phase system. Single-phase machines are usually built as 3-phase units with one idle winding, the capacity of the unit being approximately 1.73 times the rated single-phase output. In the range of sizes considered the cost of single-phase capacity is about 1.5 times the cost of 3-phase capacity. In addition, 25-cycle synchronous condensers cost approximately 50 per cent more than 60-cycle units. The 25/60-cycle cost ratios of 2.4 for shunt capacitors and approximately 2.25 for single-phase synchronous condensers indicated that the general economic considerations in the proposed application were comparable to those in 60-cycle 3-phase installations.

A rotating machine would have required the installation of 11/22-kv transformer capacity corresponding to the machine rating because studies showed the reactive source should be connected to the 22-kv trolley feeder system. Considering this factor, the cost of a 10,000 kva synchronous condenser installation, without capitalized losses, would have exceeded the cost of shunt capacitors. In addition, the losses for the synchronous condenser installation were

Figure 3. Typical line voltage at Woodmont before installation of capacitors



at least 350 kw higher than the capacitor station losses.

Other factors also favored a choice of shunt capacitors for the additional reactive source. As the traction system is single-phase, 3-phase power is not available for starting synchronous condensers excepting at the power sources, which meant that rotating machines would have presented a starting problem. It would have been necessary to bring 3-phase power to the synchronous condenser location or to install a single-phase traction motor for starting purposes, both of which meant higher cost. It was also decided that a rotating machine, if used, should have completely automatic features in order to save the cost of three shifts of operators.

The superior voltage regulating characteristics of a rotating machine were considered carefully. However, as the shunt capacitors would operate normally in parallel with regulated machines, it was decided that manually or automatically switched capacitors would give the desired system voltage control. Lagging capacity was not required so this advantage of a synchronous condenser received no consideration. Based on these factors, it was decided that shunt capacitors were the economic solution to the power system problems.

General Characteristics of Capacitor Installation

The network calculator studies showed that 10,000 kva of shunt capacitors distributed along the eastern part of the electrified section would give the desired improvements in system voltage. However, as suitable level land area was available only at Woodmont, midway between Devon and New Haven, it was decided to make the entire installation at this location. Further studies on the calculator

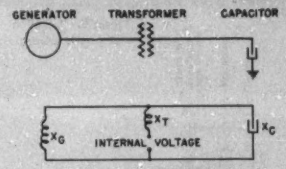


Figure 5. Equivalent circuit of elementary system at harmonic frequency

indicated that the general voltage improvement would be nearly as good with all capacitors located at Woodmont as with the units installed at several locations.

It was recognized that 10,000 kva in reactive connected to the 11-kv trolley-rail circuit would seriously overload the two 2,000-kva balancing transformers at Woodmont. For this reason, it was decided to install the capacitors in two major groups, 5,000 kva across the trolley-rail circuit and 5,000 kva on the feeder-rail circuit. A decision was made to switch the capacitors in 2,500 kva steps in order to limit the voltage change, due to switching, to approximately five per cent. Shipping limitations made it desirable to design the individual outdoor capacitor housings for a maximum of 1,250 kva at 25 cycles. The final station design was, therefore, based on eight individual units of approximately 1,250 kva, each, controlled with four circuit breakers.

The rated voltage of the capacitor station was set at 10.8 kv. This allowed the use of three 3,600-volt capacitor units in series and represented the base voltage, which if increased by 10 per cent (the limit of overvoltage allowable on the capacitors) would not exceed 11.9 kv, the maximum safe operating voltage of the older motive power.

The nature of the voltage fluctuations on the power system (see Figure 3) raised some question relative to the application of automatic voltage control to the capacitor banks. However, as it was deemed essential to have overvoltage relays and timers to remove the capacitors from service in case of sustained overvoltage, it was decided also to install the necessary undervoltage relays and timers required for complete automatic control. The control could then be used in a manner determined by operating experience.

Harmonic Control

A transformer in order to generate a sine wave voltage must draw an exciting current containing harmonic components, the magnitudes of which are a function of the grade of iron in the core and the flux density at operating voltage. Figure 4[3] shows the order of magnitude of these harmonic components for a typical grade of transformer steel. These harmonic currents normally flow between the transformers and the power sources and may cause interference with communication circuits. This factor is particularly important on a single-phase system because no low-impedance path is provided for the third harmonic currents within the transformers, and these currents must flow back to the supply points. As the New York, New Haven, and Hartford Railroad system has over 180,000 kva in station and line transformers, not including transformers on the locomotives and motor cars, it was deemed advisable to give careful study to harmonics and their control in the application of the 10,000 kva in shunt capacitors. A shunt capacitor installation cannot generate harmonics but can cause a redistribution of the harmonic currents in a system, and can increase the harmonic content of the system voltage if resonance exists between the capacitor bank and the power sources and loads. Preliminary calculations indicated a possibility of resonance so it was decided to make a harmonic study on the

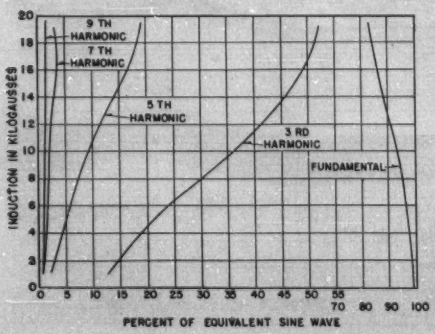
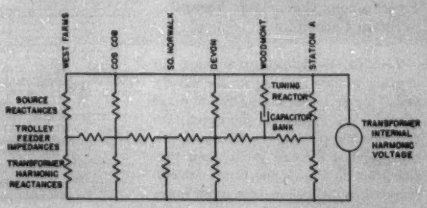


Figure 4 (left). Harmonic content of exciting current for a particular grade of silicon steel

Figure 6. Representation of trolley-feeder system for third harmonic study



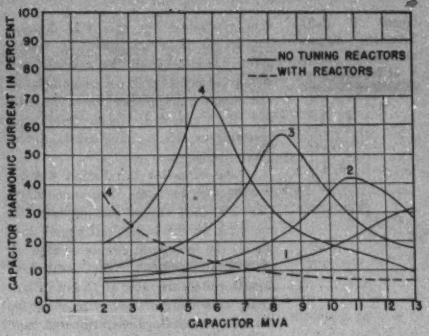


Figure 7. Third harmonic currents obtained on network calculator

Machines out of service:
1. None
2. One at Devon
3. Both at Devon
4. Station *A* and both at Devon

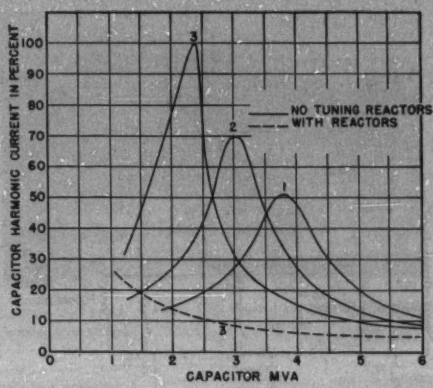


Figure 8. Fifth harmonic currents obtained on networks calculator

Machines out of service:
1. Station *A*
2. Station *A* and one at Devon
3. Station *A* and both at Devon

network calculator. The method of analysis proposed by Feaster and Harder[3] was used in making the investigation.

In this method, a transformer is represented by an internal voltage and a reactance at each harmonic frequency (see Figure 5). Normally the harmonic reactance of the transformer is large by comparison to the source reactance at harmonic frequency so only a small fraction of the internal harmonic voltage appears at the transformer terminals. With large banks of shunt capacitors, it is possible to have parallel resonance between the capacitor bank and source reactances at a particular harmonic frequency.[4] This harmonic component of the transformer exciting current is then blocked because the system impedance as viewed from the transformer terminals is high, and, in the extreme case, all of the transformer internal harmonic voltage can appear at the transformer terminals.

Transformer internal voltage and reactance data are not generally available and usually must be estimated. Fortunately, in most applications, it is not necessary to know these values accurately, but only to have reasonably correct ratios between the harmonic voltages and reactances. The latter can be estimated from the transformer exciting current and the data in Figure 4. In the study, herein reported, the exciting currents were obtained from the transformer manufacturer, and the third and fifth harmonic components estimated from Figure 4 at 50 and 20 per cent, respectively, of the total exciting current. The transformers were assumed to have internal voltages of 50 and 30 per cent at the third and fifth harmonic frequencies, and the internal reactances adjusted to give the desired harmonic currents on short circuit. These calculations are summarized in Table II. The station and line autotransformers were concentrated at five locations, with operating voltages of 11.5-kv at the major generating stations, and 11-kv at other locations. The equivalent circuit used in the network calculator study is shown in Figure 6 for the third harmonic frequency. The line and source reactances are three times the corresponding 25-cycle values. The source reactances are based on maximum generating conditions; other generating capacities as indicated in Figure 7 were considered. The shunt capacitor kva at Woodmont was varied between 2,500 and 13,000, and the capacitor harmonic current measured for each value. The results of this study are summarized in Figure 7. It will be noted that resonance is indicated by the high-peak currents for certain combinations of capacitor megavolt-amperes and generation. For example, with no machines in service at Devon and Station *A*, the third harmonic current approaches 70 per cent of rated current of the capacitor bank. The harmonic voltage at the capacitor location is for each case one third of the harmonic current, the capacitor bank reactance being equal to 33.3 per cent at the third harmonic. The large harmonic currents predicted were not considered particularly objectionable, but the harmonic voltages, which approach 24 per cent were higher than could be tolerated. A second study was made with the capacitor bank tuned to resonance with a series

Table I. Power and Reactive Sources

Station	Equipment	Rated Kva	Kw	Rkva
Cos Cob	3—12,860 kva generator	38,500	27,000	27,000
Consolidated Edison Company (West Farms)	4—7,500 kva transformers	30,000	23,000(a)	12,000
Connecticut Light & Power Co. (Devon)	2—7,200 kva frequency changer sets	14,400	5,000(b)	12,000
East New York	1—7,200 kva frequency changer set	7,200	5,000	5,000
Station *A*	1—7,200 kva frequency changer set	7,200	(c)	5,000
Total		97,300	60,000	51,000

(a). Contract maximum hour. Can take swings to 38,000 kw.
(b). Contract maximum hour. Can take 5,000 kw additional excess demand. Second unit is used as condenser during peaks.
(c). No longer a source of power. Used as synchronous condenser only.

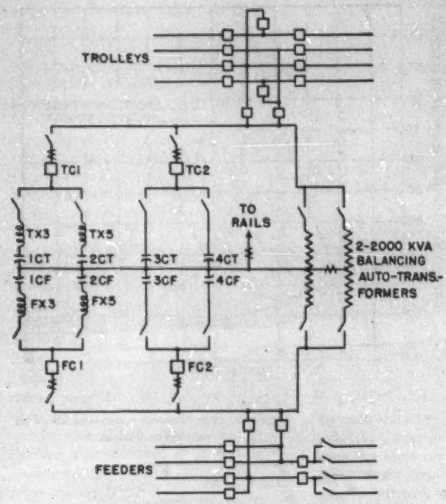


Figure 9 (left). Schematic diagram of capacitor station at Woodmont

Table II. Transformer Harmonic Currents

Station	Transformer Megavolt-amperes: Station Auto transformer	Transformer Megavolt-amperes: Line Auto transformer	Exciting Current: Station Auto transformer %	Exciting Current: Line Auto transformer %	Third Harmonic: Current amperes	Third Harmonic: Reactance ohms	Fifth Harmonic: Current amperes	Fifth Harmonic: Reactance ohms
West Farms	43.9	29	2.78	4.14	110	50	44	75
Cos Cob	43.2	16	2.78	4.14	84.5	65	33.7	98
S. Norwalk		14	1.96	3.55	222.6	244	9.0	366
Devon	14.4	12	1.96	3.55	32.3	170	12.9	255
Station A	7.2	4	1.96	3.55	12.9	425	5.2	636
Total	108.7	75			462.3		104.8	

Operating voltages: West Farms and Cos Cob—11.5 kv.
Other stations —11.0 kv.

reactor, the harmonic current thus obtained being indicated by the dashed curve in Figure 7. The reactor prevents any possibility of resonance between the capacitor bank and the system, and limits the harmonic currents in the capacitors. The reduction in harmonic voltage is considerably greater than the reduction in current because the impedance of the tuned bank is very low, being zero for the case of exact tuning and zero losses.

Figure 8 summarizes a similar study at 125 cycles, the fifth harmonic frequency. Resonance is also indicated at this frequency so additional studies were made with the capacitor installation tuned to resonance. Tuning gives the same large reduction in harmonic currents and voltages as were obtained in the third harmonic studies.

Based on these network calculator studies, it was decided to tune the capacitor installation for both the third and fifth harmonic frequencies. Consideration was given also to varying the capacitor kva with generation conditions to prevent resonance but this was deemed impractical from an operating standpoint. Trouble from the seventh and higher harmonics was not contemplated so tuning for frequencies above the fifth was not provided. Field tests and operating experience have substantiated these conclusions.

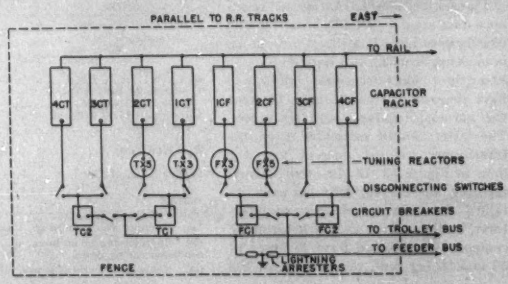
As the capacitor installation was to consist of eight banks of approximately 1,250 kva each, tuning was provided for one bank on the trolley-rail circuit and one on the feeder-rail circuit at each of the two frequencies. The individual capacitor units are rated at 15 kva with a 35 per cent margin above this value to allow for over-voltage operation and harmonics.

The units are operated at only 6.25 kva fundamental frequency at 25 cycles so have considerable margin for harmonic currents.

Description of Installation

The arrangement of the equipment included in the station is illustrated in Figures 9 and 10. In these diagrams, 1*CT*, 2*CT*, 3*CT*, and 4*CT* represent the capacitor banks connected to the trolley-rail circuits; and 1*CF*, 2*CF*, 3*CF*, and 4*CF* those connected to the feeder-rail circuits. There are eight racks in all, each of approximately 1,250 kva capacity. In series with banks 1*CT* and 2*CT* are reactors *TX*3 and *TX*5, these being the tuning reactors for the third and fifth harmonics respectively. These reactors are connected to the trolley bus through circuit breaker *TC*1. Similarly, capacitor banks 1*CF* and 2*CF* are in series with reactors *FX*3 and *FX*5, respectively, both reactors being connected to the feeder bus through circuit breaker *FC*1. Capacitor banks 3*CT* and 4*CT* have no

Figure 10. Physical arrangement and designation of equipment



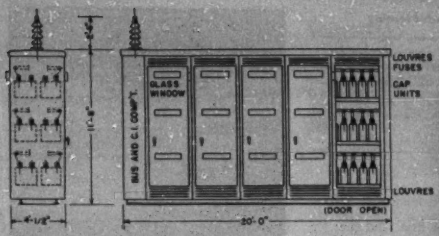


Figure 11. A 1,250-kva capacitor housing

Figure 13. General appearance of capacitor station

tuning reactors and are connected to the trolley bus directly through circuit breaker *TC*2. Similarly, banks 3*CF* and 4*CF* are connected directly to the feeder bus through circuit breaker *FC*2.

The capacitor units are in steel housings approximately 20-feet long 12-feet high, and 4-feet wide. On each side are five doors hinged vertically, which give access to all the capacitor units. The interior of these houses contain three shelves, each insulated from ground and from each other, on which are installed the capacitor units. All the units along one side of the housing on each shelf are in parallel, and the three shelves are in series. The units on the opposite side of the housing are also in parallel on each shelf, with the three shelves in series. Thus, there are two similar groups of capacitors arranged in series-parallel in each housing. These two groups are connected to a single high voltage roof bushing at one end of the housing, while the low voltage connections of the two groups are made to the rail bus through a differential current transformer, Figure 12.

Each capacitor unit is provided with an external 15 ampere fuse, and an internal discharge resistor to decrease the capacitor voltage to a safe working level within five minutes after the unit is de-energized. A discharge coil across each parallel group will drain the units within a few seconds, which minimizes the inrush currents if a bank is re-energized within a short time after its removal from the system.

The entire installation consists of 1,694 15-kva 60-cycle units rated at 6.25 kva at 25 cycles. The number and voltage rating of the units in each housing are listed in Table III. The voltage rating of the tuned banks is higher than the rating of the untuned banks because of the voltage rise produced by the tuning reactors.

The reactors are air-core oil-insulated in cylindrical steel tanks equipped with radiators for self-cooling. The third-harmonic reactors are rated at 156 amperes and have taps for inductances of 70.0, 72.3, 75.0, and 78.2 millihenrys. The fifth-harmonic reactors are rated at 142 amperes, with inductances of 21.4, 21.9, 23.0, and 23.7 millihenrys.

The circuit breakers are 500,000-kva 600-ampere 34.5-kv, type *GO-2S*, equipped with deion grids. The circuit breakers are solenoid operated from the existing 125-volt station battery. Bushing current transformers are provided for differential protection of the busses and overcurrent protection of the reactors, disconnecting switches, and capacitor banks. All protective relays are located in a control cabinet on the outdoor circuit breakers. The manual control switches for operation of the circuit breakers are on the local control switchboard in the adjacent signal tower. Normally these control switches are in the automatic control position, whereby the circuit breakers are operated automatically by master over- and undervoltage relays (mounted on the control switchboard) which operate the individual timing relays in each circuit breaker for tripping or closing.

When the voltage rises above 11.8 kv, the overvoltage relay operates an auxiliary relay which seals itself in and causes all four circuit breaker tripping timing relays to start. These are so timed that *FC*2 circuit breaker opens in 1/2 minute; *TC*2 in 1 minute; *FC*1 in $1^1/_2$ minutes; and *TC*1 in 2 minutes. Should the voltage drop below 11.4-kv during any part of this time sequence, the master

Figure 12. Diagram of connections, 1,250-kva capacitor rack

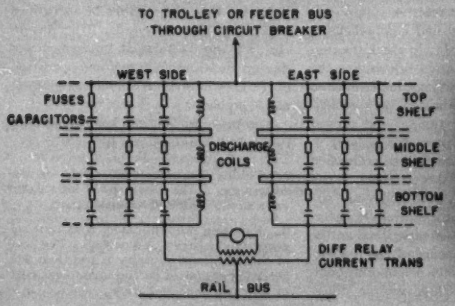


Figure 14. Capacitor housings with circuit breaker in right foreground

Table III. Number and Voltage Rating of Capacitor Units in Each Housing

	1 CF and 1 CT		2 CF and 2 CT		3 CF, 3 CT and 4 CF		4 CT	
	Units	Volts	Units	Volts	Units	Volts	Units	Volts
Top Shelf	74	4,160	74	4,160	70	3,600	72	3,600
Middle Shelf	74	4,160	64	3,600	70	3,600	72	3,600
Bottom Shelf	74	4,160	64	3,600	70	3,600	72	3,600
Total Units	222		202		210		216	

The units on each shelf are in two groups—half on each side.

voltage relay drops back, breaking the seal on its auxiliary relay and all timing relays are reset. If all circuit breakers are open and the voltage drops below 10.8-kv, the undervoltage master relay operates an auxiliary relay which seals in and causes all four circuit breaker closing timing relays to start. These are so timed that *TC*1 circuit breaker closes in 1 minute; *FC*1 in $1^1/_2$ minutes; *TC*2 in $2^1/_2$ minutes; and *FC*2 in 3 minutes. If the voltage rises above 11.3-kv during any part of this time sequence, the master voltage relay will break the seal of its auxiliary relay, and all the timing relays are reset. It will be noted that the tuned groups are taken off the line last, and restored to service first, and that the trolley-rail groups are left in service in preference to similar feeder-rail groups.

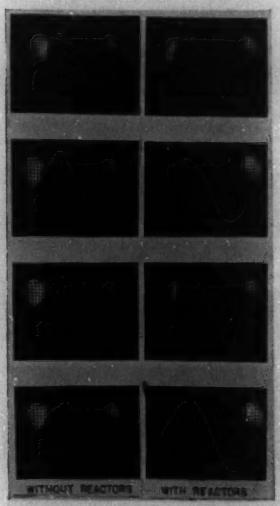


Figure 15. Typical trolley-rail voltage wave forms

Control circuit breakers closed:
Line 1—*TC*1
2—*TC*1 and *FC*2
3—*TC*1, *FC*1 and *TC*2
4—All

Should the differential or overcurrent relays operate to trip a circuit breaker, that circuit breaker is opened and locked out by a "lock-out" relay, and cannot be restored to service from the control switchboard until after maintenance men have reset the lock-out relay at the circuit breaker.

Lightning arresters, type *SV*, 15-kv, are connected to the trolley and feeder busses supplying this equipment. All of the capacitor housings, reactors, and circuit breakers are mounted on concrete pads, raised slightly above the ground, which is covered with fine crushed stone. The busses, disconnecting switches, and lighting fixtures are supported on steel poles and structures. All control wiring is in underground conduit. The yard is entirely enclosed by an eight foot wire mesh fence. Adequate illumination is provided at all equipment for night maintenance.

The entire capacitor station cost was less than $23 per kilovolt-ampere, installed, including all engineering. Figures 13 and 14 show the general appearance of the completed station.

Tests and Operating Results

The station was placed in service on March 14, 1947 and operated experimentally for the next two months to determine proper relay settings and co-ordination with the other reactive supply stations. Very careful and comprehensive tests were made in May to check the tuning of the reactors and to determine the effects on the neighboring communication lines. The tests indicated very conclusively the need for tuning, and proved that the capacitors presented nothing in the nature of an inductive co-ordination problem. Several short circuit tests were made in July, on a trolley-rail circuit near the capacitor station, to determine the effect of the capacitors on the interrupting performance of the line circuit breakers and on the neighboring communication lines. There was no difference found by study of oscillograms of the fault current and the voltage induced in nearby telephone circuits at the time of the fault between the conditions with and without the capacitors connected to the system.

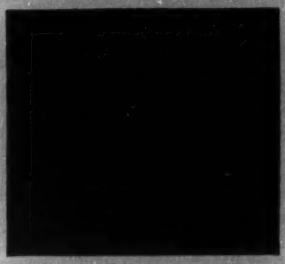


Figure 16. Oscillogram showing circuit breaker performance during charging current interruption

Third and fifth harmonic voltages of 18.4 and 11 per cent respectively, were measured with the tuning reactors short-circuited and with the Woodmont trolley-rail voltage at 11.8-kv. The maximum harmonic voltages recorded with the reactors in service were 1.2 and 0.19 per cent for the third- and fifth-harmonics, respectively. A study of the system voltage with a cathode-ray oscilloscope showed that the phase relations between the fundamental and harmonic frequencies changed with system conditions. Typical trolley-rail voltage wave shapes are illustrated in Figure 15.

Oscillograms were also made of the surges between capacitor groups when one group was switched on and off. As shown by the oscillogram in Figure 16 the circuit breaker interrupts the capacitor current without any evidence of restriking. The contacts of the *FC*2 and *TC*2 circuit breakers were inspected in October after over 2,000 operations. These inspections indicated that the number of operations expected (which will average 10 per day on the *FC*2 and *TC*2 circuit breakers, 5 per day on *FC*1 and an occasional operation of *TC*1) will present no serious maintenance problem.

The capacitor station has successfully fulfilled all expectations by:

1. Raising the trolley-rail voltage on the eastern end of the system by at least 2,000 volts during peak load conditions.

2. Releasing an estimated 6,000 kw of generating capacity at Cos Cob. The cost of this additional capacity is less than $40 per kilowatt, based on the investment cost of the capacitor installation.

3. Reducing the number of hours of operation of the three frequency changers at the eastern end of the system, with a possible saving in losses of at least $20,000 per year.

4. Reducing the transmission system losses by supplying reactive requirements closer to the load centers.

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3. System Lower-Harmonic Voltages, Methods of Calculation and Control, by Capacitors, W. C. Foster, E. L. Harder. AIEE Transactions, volume 60, 1941, pages 1060–6.

4. Experimental Study of the Probability of 5th and 7th Harmonic Resonance in Transformers, A. Boyajian. *General Electric Review* (Schenectady, N. Y.), volume 29, March 1926, page 175.

Discussion

R. R. Beisel (Gibbs and Hill, Inc., New York, N. Y.): In so far as the construction of the capacitor substation is concerned, the installation of the oil circuit breakers, reactors, bus structures, metering and protective devices did not present difficulties out of the ordinary.

However, the installation of capacitors presented a special problem due to their unusual size and weight. The capacitor houses were 11 feet 8 inches high, 20 foot long and 4 foot wide, and weighed $5^1/_2$ tons each.

Delivery of the capacitor houses to the site of erection by truck was not possible because of light wooden bridges and unsuitable unimproved country roads. The equipment, therefore, had to be delivered over the railroad's 4-track main line on a flat car to the job site, necessitating the removal of normal railroad traffic from the tracks used. Moreover, insufficient clearance under the signal cables presented a problem in unloading.

Careful study of conditions revealed that it would be possible to place the equipment on the previously prepared foundations by means of a heavy truck crane with a boom of predetermined length. The train positioned the equipment under the boom tip, and a specially prepared cradle was lowered and secured to the capacitor house. The house was lifted clear of the flat car, the car pulled out from under and the house lowered to a point where it would be just clear of the rails, switch rods and miscellaneous cable boxes. The boom was then swung until the tip was brought to the heavy messenger supporting the signal cables overhead. Two men then scaled the boom and raised the cables with their shoulders sufficiently to permit swinging the boom tip underneath the cables. It was then possible to place the capacitor house on its foundation without further interference of the signal cables with the boom of the crane.

In this manner the equipment was unloaded without incident in a relatively short time. In one case when it was imperative to clear the track for traffic, the entire operation was completed in seven minutes.

S. S. Watkins (Gibbs and Hill, Inc., New York, N. Y.): The voltage charts in Figure 3 of the paper give some indication of the problems that were encountered in the control of the capacitors. The automatic control described in the paper was the result of considerable study of the railroad's power distribution system and the expected effect of the capacitors. Some features of that study will be described here.

In order to realize the expected benefits of the installation it was important that, for as great a proportion of the time as possible, the capacitors remain connected and the voltage be held above 10.8 kv. On the other hand, avoidance of injury to either the motive equipment or the capacitors required that the upper limit of sustained voltage be held at 11.9 kv.

Studies on the network analyzer had indicated a voltage improvement of more than 500 volts at Woodmont for each 2,500 kva of capacitors connected at that point, or more than 2,000 volts for the entire 10,000 kva. During predictable periods of light load, scheduled shut-down of frequency changer sets was expected to offset much of the voltage rise caused by the capacitors, so that the average voltage would be within desired limits. Reduction in hours of operation of frequency changer sets was one of the main objects of the capacitor installation. But the pattern of frequent and severe voltage changes caused by train operation would remain. It was apparent that capacitor switching would be necessary. As all circuit-breaker operations at Woodmont are performed by a signal tower operator on telephone orders from the power director, manual control alone would not have been practical.

Quick response control comparable to that of a feeder voltage regulator would have cut the capacitors in and out much more frequently than was either necessary or desirable, and would have required contactors for this switching service. It was not necessary that the voltage be smoothed out but only that it be held within the stated limits. As fault protection required the installation of circuit breakers of 500,000 kva interrupting rating, economy dictated that these be used for all switching if possible.

Automatic control using an overvoltage relay, an undervoltage relay, and auxiliary timing relays, as described under the heading "Description of Installation" of the paper, was finally adopted. This method would avoid operation on momentary swings and would be sufficiently flexible to permit adjustment of settings as indicated by operating experience. The decision to switch in banks of about 2,500 kva, causing voltage changes between 500 and 600 volts, gave assurance that hunting could be avoided, as the spread between the proposed upper and lower voltage limits was about 1,000 volts.

The decision to use the circuit breakers for voltage-control switching emphasized the importance of keeping switching operations to a minimum. Studies were made to estimate the performance of the proposed automatic control, while consideration was still being given to the installation of a 2,500-kva bank at each of four locations. Voltage charts from each location were studied through an 8-hour period of a typical day. Suitable adjustments of these charts were made for the effect of frequency changers that would be shut down and for the effect of cutting capacitor banks in and out. Several trial values of voltage and time were used before the number of circuit breaker operations was satisfactorily reduced. The study nearest to the conditions of the final installation used undervoltage settings of 10.3 kv with $2^1/_2$ minutes delay and overvoltage settings of 11.8 kv with 5 minutes delay. This study indicated that the circuit breaker at Woodmont would operate about 10 times in 24 hours, those at the other points less frequently. It indicated maximum swings of 12.25 kv or less and average voltages of 12 kv or less while the capacitors were on. These results gave promise of satisfactory performance of the proposed automatic control, as the opinion had been obtained that the circuit breakers could stand 35 operations per day without excessive annual maintenance.

After decision had been made to locate the entire 10,000 kva of capacitors at Woodmont, consideration was given to automatic sequencing such as to equalize the numbers of operations of the several circuit breakers. However, it was decided that sequence control by the settings of the timing relays for each circuit breaker would be more suitable. The desirable sequence was governed by two considerations, first that tuned banks be in service whenever any capacitors were connected, and second that the trolley-rail banks be in service first and out last in preference to similar feeder-rail banks. These considerations established the sequence described under the heading "Description of Installation" of the paper, which necessarily would produce differences in the numbers of circuit breaker operations. The expected differences were accepted, as it appeared that the number of operations on any one circuit breaker would not be excessive.

The method of estimating performance of automatic control will be described here, using for simplicity the actual installation of the four 2,500 kva banks at Woodmont. The Woodmont trolley-rail voltage chart for a typical day before the capacitor installation was used for the study. At the starting time on the chart all capacitors were assumed as connected. Frequency changers were assumed to be taken off as load conditions permitted. Previous studies on the network analyzer had shown the net effect of these switching operations, in terms of net volts added to or subtracted from trolley-rail volts at Woodmont. Adjustment was made, not by redrawing the voltage graph, but by moving the voltage scale in the direction opposite to that of each voltage adjustment. A duplicate of the voltage chart scale was drawn at the left hand edge of a piece of cardboard and the upper and lower voltage relay settings were marked conspicuously. This sliding scale was placed at the starting time on the voltage chart and

moved vertically by the amount of the net adjustment required, and then was moved horizontally in the direction of time. When the voltage graph rose above the upper setting on the sliding scale the first timing relay was assumed to start. When the graph had remained above the setting for a time equal to the setting of the first timer, the first 2,500-kva bank of capacitors was assumed cut out and the sliding scale was raised 500 volts. If the graph was still above the upper setting, time continued to be counted for the timer of next longer setting until either the next 2,500-kva bank was cut out or the voltage graph dropped below the setting on the sliding scale. Whenever the voltage graph dropped below the lower setting the converse of the foregoing procedure was followed. As long as the graph remained between upper and lower settings the capacitors were assumed to remain unchanged. Records were made of each operation in or out and the results presented in graphic form.

In the studies that actually were made, voltage charts for each of four locations were used and voltage adjustments by sliding scales were made at each location whenever switching occurred at any one of them. The studies of expected performance of automatic control were used as guides in determining the voltage and time settings that were applied before the trial operation on March 14, 1947.

On March 14 and 17 graphic voltage charts and observations of current, voltage, and circuit breaker operations were made. The number of circuit breaker operations was even less than anticipated, while duration and extent of uncorrected voltage changes indicated need for more frequent operation. During periods of uncorrected swings of overvoltage the readings of current through the tuned banks showed a marked increase beyond the values attributable to 25-cycle admittance. This obviously resulted from increase in the harmonic components as transformers on the system became overexcited by the higher voltage. This explanation was corroborated by the increasing distortion observed on the oscilloscope screen.

Accordingly the time settings were shortened. After further observation of results the settings recorded under the heading "Description of Installation" of the paper were established as satisfactory. In conclusion it may be remarked that the voltage relays for automatic control for this application require relatively narrow bands between pickup and dropout. Bands much wider than the 4 and 5 volts previously stated, would make the control more sensitive to momentary swings and might decrease the effective band between the overvoltage relay and the undervoltage relay to the point where pumping would occur.

H. F. Brown: The discussions by Watkins and Beisel amplify some of the special engineering and construction problems encountered in the design and erection of this capacitor installation.

In conclusion it may be stated that this capacitor installation has proved to be a very satisfactory and economic solution of the various problems with which the railroad was confronted: low voltage at the eastern end of the system; and concentration of reactive on the central and eastern power supply points, which limited the potential output of the central supply station generators.

Unusual Performance of Standard 230-Kv Impulse and Tank-Type Oil Circuit Breakers on Field Tests

E. B. RIETZ
ASSOCIATE AIEE

Synopsis: The requirements of the United States Bureau of Reclamation for 230-kv circuit breakers at Grand Coulee Dam which would far exceed present-day standard circuit-breaker ratings, required field tests to check the performance of standard circuit breakers in order to predict the required designs for higher ratings. Standard 3,500 megavolt-amperes (mva) low oil content impulse- and tank-type oil circuit breakers were tested on the maximum capacity available of over 7,000 mva. Usually satisfactory performance was observed under all test conditions indicating that standard circuit breakers of these types incorporating modern methods of arc interruption, will require but little change to adapt them to the Grand Coulee Dam requirements for ratings of 10,000 mva.

EARLY LAST YEAR, power circuit breaker engineers were told by the Bureau of Reclamation that the requirements of 230-kv circuit breakers for Grand Coulee Dam, would involve circuit breakers for duties of approximately 10,000 mva. This was a conclusion that had been reached after a comprehensive design and network analyzer study to determine the most desirable bussing arrangement for the Grand Coulee Power Plant and to avoid the use of 230-kv current limiting bus tie reactors. At that time, the maximum standard 230-kv circuit-breaker rating was 3,500 mva. Although factory tests of new interrupters had indicated that 5,000-mva circuit breakers were feasible,[1] no work had been done to design them because of lack of demand. The thought of a circuit breaker of twice that rating was certainly one to trick the imagination. However, it was soon realized that the higher rated circuit breaker presented the best over-all approach to the problem and consideration was given to arriving at a satisfactory solution. The agreement of the Bureau of Reclamation that such a solution could be expedited by providing facilities for field tests at Grand Coulee Dam Power Plant lead to the program that is being described in this and companion papers.

Consideration of Existing Circuit Breaker Designs

The General Electric Company had two designs of circuit breakers which could be considered. The first was represented by the low oil content impulse-type circuit breaker, and the second, by the conventional tank-type circuit breaker with multibreak interrupters.

Impulse-Type Circuit Breaker

The impulse circuit breakers had an outstanding record. The basic design was created in the early 1930's for 3-cycle 287.5-kv circuit breakers for the Boulder Dam lines.[2] The first of these circuit breakers went into service in 1934. At the same time, 138-kv circuit breakers of similar design were installed at the Los Angeles end. Five years later, additional circuit breakers of both ratings were installed. About the same time, 230-kv circuit breakers were designed and built for the Pacific Northwest. At the present time, approximately 50 impulse circuit breakers are in service. Their record has been exceptionally satisfactory. Interrupting performance has been unquestioned. They have demonstrated a very desirable interrupting characteristic on line charging currents where interruption is positive and restriking is unknown. Interrupting times are within 3 cycles from 0 to 100 per cent of rating. Operating experience has also been very satisfactory. Maintenance has been reduced because a minimum quantity of oil is subject to deterioration from arcing.

The performance to be expected on the higher duty could be extrapolated from two sources: (1) the exceptionally satisfactory field record of fault interruptions and (2) factory tests. Although designs had been built for 3,500 mva at 287.5 kv and for both 3,500 and 4,000 mva at 230 kv, the majority of the 230 kv experience centered around 2,500-mva rated circuit breakers on the system of the Bonneville Power Administration. Two of the 3,500-mva 230-kv rated circuit breakers had been in service at Grand Coulee Dam for about two years. The pertinent observations from field experiences centered around the excellent condition of the oil and contacts after numerous interruptions leading to the conclusion that they had been accomplished easily and with little generation of arc energy. Factory tests gave no indication of approaching the limits of capacity. On these bases, the impulse circuit breaker appeared to have a large margin of reserve and was certainly worthy of further consideration.

Tank-Type Circuit Breaker

The modern tank-type circuit breaker represents a product of years of evolution that in itself bespeaks a series of improve-

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E. N. Rietz is with the power circuit breaker division of the General Electric Company, Philadelphia, Pa.

The author speaking for the General Electric Company would like to take this opportunity to congratulate the United States Bureau of Reclamation and its engineers on their progressive attitude which has made possible these field tests so that circuit breakers best suited to their requirements can be developed. Acknowledgement is also made of the interested co-operation of the Bonneville Power Administration in arranging for these tests to be made on their power system so that maximum fault conditions could be obtained.

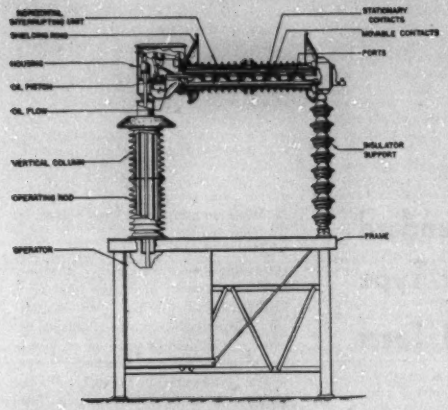


Figure 1. Cross-section of a pole of the 230-kv low-oil-content impulse circuit breaker showing the oil piston which moves oil between stationary and movable contacts on opening

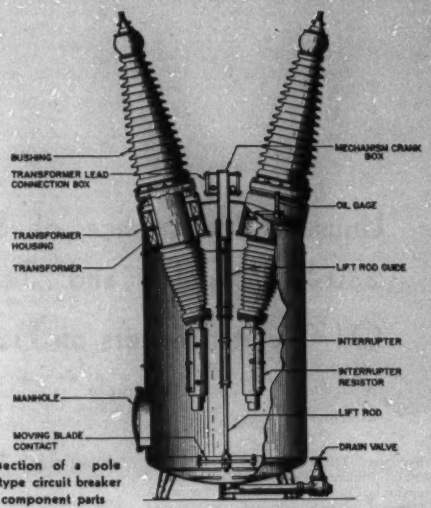


Figure 2 (right). Cross-section of a pole unit of the 230-kv tank-type circuit breaker showing relationship of component parts

ments that ought to reflect in a capacity beyond its rating. Each new interrupter, although usually created to take care of a specific condition on which its predecessor failed, had more capacity to handle the higher duties. Recent requirements for higher speed clearing and reduced oil deterioration resulted in interrupters having such short arc durations that pressures and other mechanical stresses were automatically reduced. Modern 3-cycle interrupters, therefore, had improved interrupting capacity because they had to interrupt in such a short time. As a result, they could be expected to have capacities beyond their tested ratings. These characteristics were present in the type *FK-439* oil-blast circuit breaker with its improved multibreak interrupters and pole unit structures.[3]

As with the impulse circuit breaker, an estimate of the tank-type circuit breaker performance on higher capacities had to be based on field experience and factory test data. Field experience covered both 230 and 138-kv multibreak interrupters in considerable numbers and rated up to 3,500 mva. Field tests had been made on both voltage ratings, the most recent series having been made in 1944 on a 3-cycle 3,500-mva 138-kv circuit breaker.[4] This proved an excellent performer and could be used as a yardstick for a 230-kv circuit breaker with similar type interrupters. Factory tests were even more complete than on the impulse circuit breaker, because unit interrupter testing was possible with the resistance voltage grading that was used across the interrupters of a pole unit. These tests indicated that the tank-type circuit breaker with modern multibreak interrupters also had an appreciable margin over assigned ratings.[3]

The Test Circuit Breakers

With two standard circuit breakers available for the new requirements, a test program was arranged to include both of them. Fortunately, it was possible to use the impulse circuit breaker already installed in the yard for one series. The second series was made on a new standard tank-type circuit breaker ordered by the Bureau of Reclamation and being shipped to the Shasta Project in California. Before being sent to Shasta, it was, therefore, routed to Coulee Dam for the field tests.

The impulse circuit breaker was a type *FG-25*, rated 3,500 mva 230-kv, 800-amperes with 3-cycle interrupting time. Figure 1 shows a cross-sectional view of a pole unit. As contrasted with other conventional oil circuit breakers, at interruption oil is forced to flow between the movable and stationary contacts by the movement of a mechanically operated piston. The circuit breaker consists of a horizontal interrupting unit with contacts and actuating linkage, a vertical column which encloses an insulated operating rod connecting the operating mechanism and the contact actuating linkage, an insulator column, and a spring charged operating mechanism. Springs in the operating mechanism are charged so that their energy is transferred to the oil piston and contacts for interruption and opening of the circuit breaker. This guarantees a positive oil flow which gives the same interrupting times from 0 to 100 per cent of rating and is particularly advantageous when interrupting line charging currents where restriking is eliminated and dangerous overvoltages prevented.

The tank-type circuit breaker was a

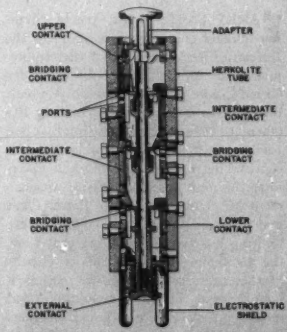


Figure 3. Cross-section of the 230-kv multibreak interrupter. Resistor unit for grading voltage and helping in interruption of capacitance current is connected between top adapter and lower stationary contact

type *FK-439*, rated 3,500 mva 230/196-kv 800-amperes with 3-cycle interrupting and 20-cycle reclosing times. This was a standard circuit breaker in current production. Figure 2 shows a cross-sectional view of a pole unit and Figure 3 a view of the multibreak interrupter. The general features were described before the AIEE two years ago.[3] This circuit breaker had 78-inch diameter pole unit tanks, an insulation level of 425-kv 60-cycle, and 900-kv impulse, improved multibreak interrupters, high speed pole unit mechanism linkages, and an air operator.

Test Program

Field tests were made on the two circuit breakers on the weekends of March 23 and April 6, 1947. A full description of the test setup and test results is contained in a paper presented by C. L. Killgore and W. H. Clagett.[5]

Reclosing tests were not made on the type *FG-25* impulse circuit breaker as it was equipped with a spring charged operating mechanism which is not arranged for ultra-high speed 20-cycle reclosing. A recently developed air operator for ultra-high speed reclosing of impulse-type circuit breakers was unfortunately not available at the time of these tests.[6]

Laboratory recording devices were installed to obtain as much related information as possible. These included: travel indicators for lift rod movement, recorders for measuring pressures in the multibreak interrupters, tank pressures below and above oil level and pressures in the interrupting unit of the impulse circuit breaker and strain gauges for mounting on the tank supports of the tank-type circuit breaker.

Test Results

The circuit-breaker performances were without exception unusually satisfactory.

The impulse circuit breaker performed as expected on the line dropping tests and there were no restrikes and no indications of switching overvoltages. The average arcing time was less than one-half cycle. Although the test schedule did not include line conditions above 260 miles in length, previous experience confirmed by these tests would indicate the same satisfactory performance on longer line lengths.

The series of 14 fault operations on the impulse circuit breaker left the contacts in serviceable condition and did not greatly affect the dielectric strength of the insulating oil. There was little vibration of the circuit breaker and no ground shock. The interrupting times are shown by Figure 4 and average 2.8 cycles. More recent designs of operating mechanisms would have faster mechanical unlatching times and would reduce these times by almost one-half cycle.

The tank-type circuit breaker had no difficulty in switching 382 miles of line, or the equivalent of 28,500 charging kva. No restrikes were obtained on the 100 and 200-mile lengths. A single restrike occurred on about 60 per cent of 300 and 382-mile tests, but the resistor unit of the multibreak interrupters limited the overvoltage to a safe value. The cathode ray oscillograph records showed nothing over 1.9 times normal. The resistor currents were interrupted in less than half the stroke of 28 inches.

The fault tests on the tank-type circuit breaker were without incident. After the first series of 14 tests, the contacts were in condition to perform a considerable number of additional interruptions, but because of the large number of tests proposed for the second series, it was considered desirable to change them.

The reclosing tests included in the second series were remarkable in that there was no visual distinction between the first and second operations of the cycle. For the last four reclosing cycles at maximum duty, modifications were made to the operating mechanism to reduce the time and these were made with 12 cycle reclosing time from trip impulse to remaking of the circuit. These tests demonstrated the interrupting ability of the multibreak interrupters on successive operations with very short time intervals. Admittedly, the duration of resistor current would have to be reduced to make 12 cycle reclosing practical but the tests indicate that the basic interrupter is available and it is believed that the reresistor problem has a fairly simple solution. The interrupting times of all tests are shown by Figure 5. On this circuit breaker also, the disturbance and ground shock were negligible.

Conclusions

The demonstrated performance of these circuit breakers on field tests, lead to the following conclusions:

1. The interrupting performances of both the impulse and tank-type circuit breakers were quite remarkable when it is considered that as standard circuit breakers they interrupted nearly twice their rating with no evidence of distress and no signs that they had reached their limits.

2. Line charging currents can best be interrupted by oil circuit breakers of the im-

Figure 4. Interrupting performance of the 3-cycle 230-kv impulse circuit breaker on field tests showing circuit breaker interrupting times at various duties

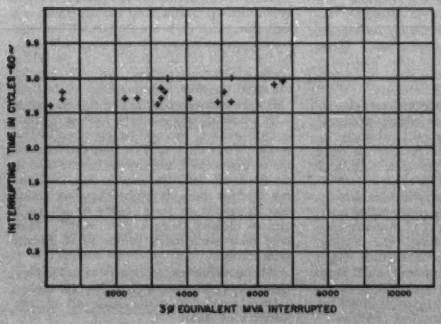
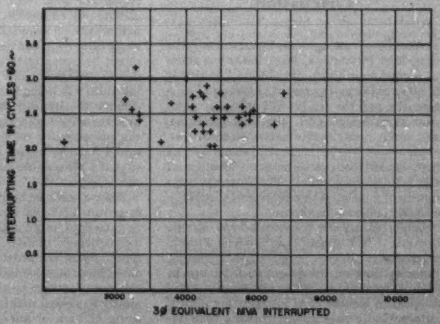


Figure 5. Interrupting performance of the 3-cycle 230-kv tank-type circuit breaker on field tests showing circuit breaker interrupting times at various duties



pulse type. However, conventional multi-break interrupters can be combined with suitable resistors to give perfectly satisfactory performance.

3. Ultra-high speed reclosing with reclosing times as low as 12 cycles, is possible at 230 kv and the higher interrupting ratings.

4. Based on these field tests, it is apparent that ratings of 7,500 mva at this voltage can easily be obtained and it is evident to design engineers that only slight changes would be necessary to obtain 10,000-mva circuit breakers.

5. Field tests are a requisite to the development and proof of rating of circuit breakers above 3,500 mva because it does not seem practical to have manufacturers' test facilities large enough to handle these super-ratings.

6. It should not be inferred from these tests that all circuit breakers will perform satisfactorily so much above their rating. There are too many factors involved in the interrupting characteristics of a circuit breaker to draw any such general conclusion.

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Discussion

A. W. Hill (Westinghouse Electric Corporation, East Pittsburgh, Pa.): Rietz has given us a good description of two types of circuit breaker construction tested at Grand Coulee recently, and the reader will naturally compare their design and operating problems, and interrupting performance, so as to conclude which will be preferred in this field of high-capacity circuit breakers. The actual test results and contemplated future ratings represent a big step beyond recent standards of 3,500,000-kva maximum.

While describing the tank-type circuit breaker, Rietz states that modern 3-cycle interrupters have increased interrupting capacity because they have to interrupt in such a short time. It would seem a little illogical to assume that interrupting ability automatically rises as interrupting time decreases, for difficulties such as shock pressure surges might actually increase, and I wonder if Rietz is not referring to the naturally higher value of short circuit current which must be interrupted early in the decrement curve.

The reader is left with an unanswered question with the statement that the resistor paralleling the interrupter constitutes a problem which has a fairly simple solution. What is the nature of this solution—to eliminate the resistor completely?

Referring to the impulse circuit breaker, the statement that maintenance has been reduced because a minimum quantity of oil is subject to deterioration from arcing, is understandable so far as time required for filtering is concerned, but the same kind of equipment is required as for tank-type circuit breakers, and in the long run it would appear that more frequent treatment would be inevitable with the relatively higher concentration of carbon.

It would be interesting to know the magnitude of pressures shown on the oscillograms but not identified in the papers, as the mechanical structure using large porcelain units frequently arouses fear of breakage under shock stresses.

It is generally recognized that increased interrupting ability may require only moderate design changes when starting from low ratings, but as we approach short circuit currents of the order of 25,000 amperes, it is not so apparent what steps the designer would take in making the "slight changes" referred to in conclusion number 4. Could these be in the direction of stronger operating mechanisms, greater oil piston output, and so forth, and if so, does this not imply greater and unproved stresses on a porcelain structure which might tend to throw the balance toward the tank type as the preferred choice?

Robert H. Nau and **Frank Fry** (University of Illinois, Urbana, Ill.): The performance of the indicated standard 230-kv impulse and tank-type circuit breakers using the general impulse interruption features have performed exceptionally well in the light of their ratings. However, it seems a foregone conclusion that the manufacturer was aware of the inherent interrupting ability of these designs; otherwise such high power tests would not have been conducted.

For the self-generated pressure interrupter in a steel tank, there is evidence from all sources that with proper design the inherent pressure and flow requirements for almost any interruption can be met. There might be some question in this particular design whether or not the voltage interrupting ability is being used effectively.

With the impulse circuit breaker, the requirements on the piston which produces the pressure for oil flow will naturally be raised for the higher kilovolt-ampere duty so that a reasonable increase in driving force can be expected.

Since a circuit breaker which is sold to interrupt 3,500,000 kva is never called upon to interrupt more than that amount, any interrupting ability above the rating, especially of the orders of magnitude which are evident here, is either purely a gift from the manufacturer to the customer or an inability to design within reasonable limits because of lack of adequate test facilities.

P. L. Taylor (Allis-Chalmers Manufacturing Company, Boston, Mass.): I want to compliment the author on his work in outlining such a marked extension of the range of interrupting capacity of power circuit breakers in the transmission class.

The effect, of tripling the interrupting capacities at the higher transmission voltages, upon power system design seems almost certain to be far reaching.

It is peculiarly fitting that these tests indicating the feasibility of oil circuit breakers up to 10,000,000 kva were made in 1947, the fiftieth anniversary year of the first oil circuit breaker, which was designed by Doctor Leonard Elden in 1897. That these fiftieth anniversary oil circuit breakers are pneumatically operated and that some of the earliest designs of the Elden oil circuit breaker were also pneumatically operated, is worthy of note.

The comments of the author with regard to oil circuit breakers seem to me to be quite significant. He has said in part:

> "The modern tank type circuit breaker represents a product of years of evolution that in itself bespeaks a series of improvements that ought to reflect in capacity beyond its rating."

It has been our experience also, that the oil circuit breaker has a way of coming through.

Again I want to compliment the author on his most important contribution.

E. B. Rietz: For a good many years circuit breaker manufacturers have tried to incorporate in their designs margins of safety to take care of the many unknown conditions involved in the problem of circuit interruption. It does not seem that this is any time to depart from such a desirable practice and, therefore, I can only say to Nau that we will continue to make circuit breakers as good as we can and if large margins of safety result we are glad to pass them on to the users.

It is true that increased laboratory test facilities take more of the unknowns out of circuit breaker performance, but there are still enough left to justify a conservative approach.

Taylor confirms the fact evident to both user and manufacturer of the very great evolution of circuit breaker designs.

The answers to Hill's questions are based on the general design characteristics of the impulse circuit breaker. The porcelain shells which are first evident to the observers are only for weather protection. All loadings during operation are taken by heavy-walled, paper-wound tubes inside of the porcelain shells. The porcelains are not rigidly attached so they can move or vibrate at their own frequency. The maintenance advantages of impulse-type low-oil content circuit breakers can best be answered by the users. They have told us that the reduced quantities of oil necessary to handle during maintenance and the simpler oil facilities required for these circuit breakers are major considerations in their desire to see such circuit breaker designs promoted. These field tests were made primarily to obtain information.

The conclusions relative to changing these circuit breaker designs for still higher ratings were not made with all of the answers at hand.

Field Tests for Development of 10,000,000-Kva 230-Kv Oil Circuit Breakers for Grand Coulee Power Plant

C. L. KILLGORE
MEMBER AIEE

W. H. CLAGETT
MEMBER AIEE

THIS PAPER discusses the field tests which were made on four types of 230-kv 3-cycle oil circuit breakers at Grand Coulee power plant at Coulee Dam, Wash. Faults of over 7,000,000 kva, far exceeding any previous field tests[1-7], were successfully interrupted on the 230-kv system.

These field tests were necessitated by an urgent requirement for the early design and development of 230-kv 3-cycle oil circuit breakers with an interrupting capacity of 10,000,000 kva, rated on a duty cycle of one 20-cycle reclosure, for installation at the Grand Coulee power plant. This unparalleled interrupting capacity is nearly three times that of the 3,500,000 kva circuit breakers which were the largest available up to the time of these tests.

Grand Coulee power plant is the largest hydroelectric power plant in the world and is situated on the Columbia River approximately 90 miles to the west of the city of Spokane, near the center of the state of Washington. The power plant is located near the electrical center of a vast transmission network extending over the states of Washington, Oregon, Idaho, Montana, and Utah. This network is represented by the Northwest Power Pool, with a present total connected peaking capability of approximately 3,500,000 kw. The Northwest Power Pool comprises an interconnection of all major operating utilities of the Pacific northwest as shown schematically in Figure 1, as well as many smaller organizations not shown, and was originated during the recent war emergency for purposes of mutual benefit through pooling of available reserves, taking advantage of load diversity over the two time zones involved, and taking advantage of the diversity of stream flows of the various watersheds in the area. The Grand Coulee power plant, designed, constructed, and operated by the United States Bureau of Reclamation, is connected to the power pool through the transmission facilities of the Bonneville Power Administration.

Grand Coulee Power Plant

Grand Coulee consists of two similar power plants on either side of the river as illustrated in Figure 2. Each power plant will ultimately contain nine of the world's largest hydro-generators each having a nameplate rating of 108,000 kw and an actual peaking capability of 130,000 kw, or a combined maximum in excess of 2,300,000 kw. The Grand Coulee pumping plant, located adjacent to the dam, will ultimately contain twelve 65,000-horsepower irrigation pump motors. The unprecedented size of the ultimate installation, representing a tremendous concentration of short-circuit capacity never heretofore combined in any one place, introduces many unique problems in circuit breaker application, while retaining necessary switching simplicity and flexibility. A unit-system wherein all bussing is accomplished on the high-voltage side of the transformer banks at the 230-kv switchyards is used at Grand Coulee. The installation at Grand Coulee at the time of the tests consisted of only six of the ultimate 18 generating units and the degree of solid bussing, without the use of current-limiting 230-kv reactors, was already limited by the 3,500,000-kva interrupting capacity of the installed 230-kv circuit breakers. The present circuit breaker limitation also dictates the temporary use of a split bus with interleaved transmission lines, illustrated by Figure 3, which also constituted the switching arrangement used during the staged interrupting-capacity tests. Synchronizing power flows between the two bus sections through the three pairs of lines with line impedance acting to reduce circuit breaker duty to below nominal equipment ratings. This split bus with interleaved lines presents a number of undesirable operating difficulties, particularly during switchgear maintenance, and is considered as an interim arrangement only.

Ultimate Bussing Scheme and Circuit Breaker Rating

Comprehensive design and network analyzer studies of system transient stability and circuit breaker duties to determine the optimum bussing arrangement for the fully developed Grand Coulee power plant, have been conducted jointly by the Bonneville Power Administration and the Bureau of Reclamation. These studies involved a great number of schemes including interleaved lines, isolated groups of lines and units, 3-winding transformer banks and 230-kv reactors for bus-ties, and others. The results of these studies will be presented in future

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C. L. KILLGORE is an electrical engineer with the United States Bureau of Reclamation, Denver, Colo., and W. H. CLAGETT is an electrical engineer with the Bureau at Coulee Dam, Wash.

The authors wish to acknowledge the fine co-operation of the Bonneville Power Administration in arranging for these tests to be made on its power system and on the interconnected system of the Northwest Power Pool. We also wish to acknowledge the fine co-operation of the Westinghouse Electric Corporation and the General Electric Company in making these tests possible. It was only through the efforts of all participants in these field tests that it was possible to obtain such successful results. The undertaking of such unprecedented field tests is a tribute to the courage and vision of the engineering profession.

papers. A 230-kv 5-section ring bus, each section capable of being isolated from the ring by sectionalizing bus-tie circuit breakers was finally selected as the most desirable because of its simplicity and operating flexibility. The ultimate ring-bus arrangement is shown in Figures 4 and 5.

However, this engineering solution was not without its practical limitations. The possible magnitude of asymmetrical short-circuit currents on such a bussing arrangement, with all generators and pump motors in operation, all transmission lines in service and all transformer neutrals solidly grounded, will approach 15,000,000 kva, assuming a circuit breaker contact-parting time of 2.25 cycles after inception of the fault and 1.35 as the system current decrement factor for a totally offset fault. In order to reduce this excessively high circuit breaker duty to a more reasonable value of slightly over 10,000,000 kva, for faults immediately adjacent to the station, certain features will be incorporated into the future station design, such as provision for the installation of transformer neutral reactors. Also, the irrigation pump motors and their associated generators may be operated independent of the 230-kv system. Ten million kva was arbitrarily chosen since it represented the lower limit in the reduction of maximum possible short-circuit currents without resorting to costly 230-kv 500-ampere, continuous rating, current-limiting bus-tie reactors. Emergency line loadings under certain abnormal conditions, required circuit breakers with a 1,000-ampere continuous current rating. A further requirement was that the circuit breakers should be capable of one 20-cycle reclosure with no de-rating factor. The development of such 10,000,000-kva breakers in one step from the presently available 3,500,000-kva designs, obviously required extensive high-capacity laboratory and field tests, the latter at full system voltage, under system conditions, out and beyond the frontiers established at the Philo plant of the American Gas and Electric Company in 1944, and at Grand Coulee in 1945 which were reported previously in earlier papers.[1,2,3,7] As the Grand Coulee power plant on the Northwest Power Pool is the only location where such a tremendous concentration of short-circuit capacity is available, and further, since this system is the only presently known customer for such high-capacity equipment, the Grand Coulee switchyard was the obvious location for the required developmental field tests.

Objectives of Tests

The immediate and specific objectives of the field tests were:

1. To supplement and extrapolate laboratory tests on present designs to higher values of fault currents under system conditions, at full system voltage, and at system rates-of-rise of recovery voltages.

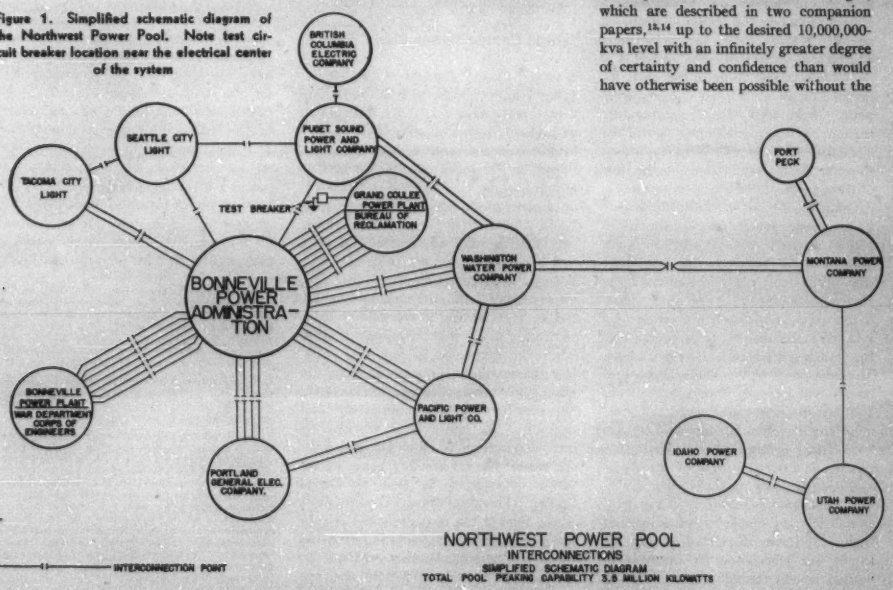
2. To determine the amount of mechanical distress suffered, if any, and other pertinent operating data during the interruption of ultra-high-capacity faults, with the view of detecting and improving any apparent weaknesses or short-comings in the equipment design.

3. To determine, if possible, the actual maximum interrupting rating of the designs being tested, under standard duty-cycle and high-speed 20-cycle or less reclosing conditions on both solid and arcing faults.

4. To determine the behavior and pertinent operating data of several types of interruptor units, with and without shunting resistors, when switching long unloaded transmission lines.

An analysis of these line-switching and high-capacity interrupting duty field tests will permit the manufacturers to extend their present circuit breaker designs, which are described in two companion papers,[13,14] up to the desired 10,000,000-kva level with an infinitely greater degree of certainty and confidence than would have otherwise been possible without the

Figure 1. Simplified schematic diagram of the Northwest Power Pool. Note test circuit breaker location near the electrical center of the system



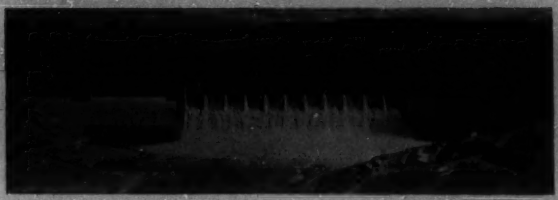


Figure 2. Grand Coulee Dam and power plants

benefit of field tests, even though the available testing capacity at this time was considerably short of the ultimate goal. One of the most valuable features of any series of field tests, particularly in the field of high-voltage switching, is in substantiating the results of previous laboratory tests, as it is economically impossible to exactly duplicate all field conditions and obtain the necessary short-circuit capacity in the laboratory at full system voltage. Adequate field tests of new equipment almost invariably result in improved designs and the elimination of weaknesses thus resulting in reduced operation and maintenance costs throughout the life of the equipment.

Station Switching Arrangement and Faults

In regard to the fault tests, it was desired that maximum system capacity be reached in several incremental steps on successive tests, rather than subject the circuit breaker to high fault currents immediately. The basic plan for this was to use one or two isolated units (500 to 1,000 megavolt-amperes) for the initial low-current tests; followed by various other incremental steps, such as one bus with three lines and two, three, or four connected generators; or both busses paralleled, with all six lines and either four, five, or six connected generators. Practically any combination was available providing all lines remained in service; not more than two machines were unloaded simultaneously; and not more than four machines were placed on either bus at any one time. In this manner almost any value of fault current could be obtained as desired, involving only a minor amount of station switching to accomplish a change from one fault current to the next.

All faults were single-line-to-ground on *A*-phase only, which greatly simplified the test connections and the otherwise lengthy and involved test program which extended through six weeks, exclusive of the time required for equipment erection and disassembly. During this time a total of 287 test oscillograms were obtained, in addition to the large number taken during preliminary checking and timing of the four circuit breakers involved.

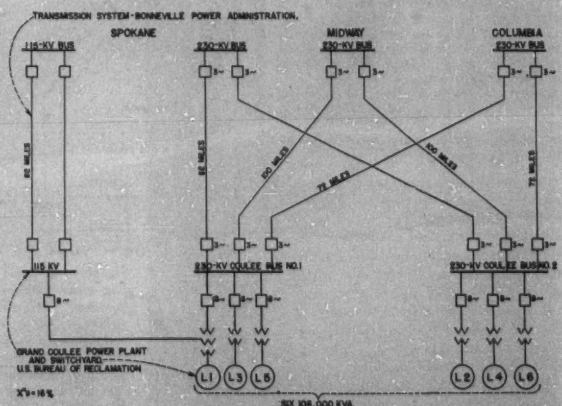
Aside from simplicity reasons, single-phase tests produced about 30 per cent higher fault current than 3-phase tests, with all transformer neutrals solidly grounded to the plant ground mat. All faults were applied directly to the switchyard ground mat, and as these two mats are solidly connected through a counterpoise of several parallel runs of 500- and 750-thousand-circular-mils buried copper cables, this continuously unbroken metallic path provided an extremely low zero-sequence impedance from the point of fault back to the transformer neutrals. This arrangement was largely responsible for the very high values of fault current which were obtained during the course of the tests, as well as the fact that the generator direct-axis, subtransient reactance is only 16 per cent for the Coulee generators.

Pretripping of the test circuit breakers was used extensively during the tests, particularly the high-capacity operations, to take advantage of the rapid current decrement due to the effect of subtransient reactance during the first one or two cycles of fault current. Positive pretripping was accomplished in all cases through a specially mounted "*a*" switch on the main pull-rod above the circuit breaker operating mechanism. In the case of the "open" operations, where the test circuit breaker starts from the closed position, the pretrip switch was mounted on the pull-rod of the adjacent station circuit breaker applying the fault. Pretripping also reduced the shock to the system during high-current tests, to the point, in fact, where the system dispatcher in several instances could not determine by observation just when the fault had been applied. This refers to the few cases where the circuit breaker was pretripped and the fault cleared in 1/2 to 1 cycle. Approximately 15 per cent was added to the circuit breaker duty over similar tests which were not pretripped.

Effect of Tests on System Equipment

Any effect on installed station and system equipment, occasioned by the high-capacity staged fault tests, was considered to be appreciably less than is

Figure 3. Simplified schematic diagram to indicate the normal method of busing the present six units with six interleaved 230-kv transmission lines, to reduce circuit breaker duty to within the present nominal circuit breaker ratings



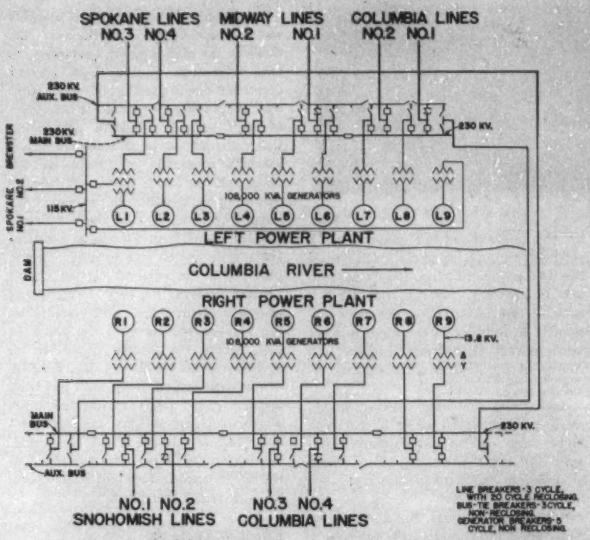


Figure 4. Ultimate Coulee 230-kv ring bus. Physical arrangement

usually encountered during the general run of ordinary line faults in a storm, for the following reasons:

1. The faults were not accompanied by overvoltage transients which are usually involved with faults of lightning origin.

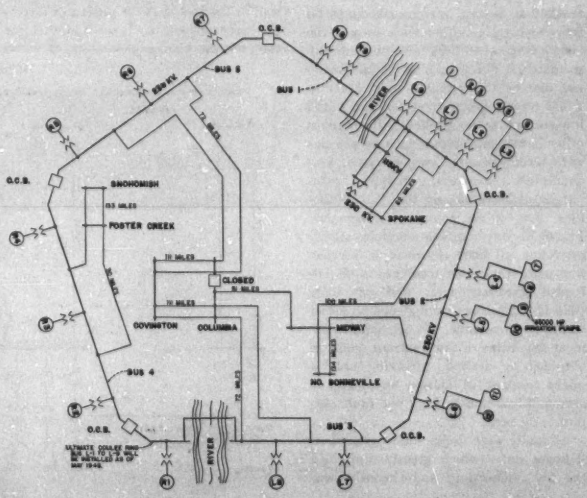
2. The circuit breaker was purposely pretripped about 1 to $1^1/_2$ cycles fast on the higher-capacity faults, thus reducing the duration of the mechanical stresses within the equipment to as much as one-half that usually encountered under normal fault clearing time. This also permitted a closer setting of the backup relays, reduced the shock to the system, reduced the possibility of a false relay operation at other points on the system, and most important, in case the fault was cleared by backup protection the reduced relay time increased the relative margin of safety in system transient stability.

Assumption (1) was confirmed by magnetic oscillograms obtained of the generator voltages at the machine terminals, revealing the complete absence of transient overvoltages during fault applications. Assumption (2) was confirmed by statements from the system dispatchers to the effect that the faults were of such short duration on the pretripped tests that they had difficulty in observing exactly when they were applied.

Aside from the fact that staged faults involve considerably less hazard to the system than the usual lightning induced line faults, it should also be pointed out that at no time during the extensive series of tests was any permanent station equipment subjected to stresses or operating conditions beyond that for which it was designed. The design specifications for the Coulee generators, for instance, required that the machines be so constructed as to successfully withstand short-circuit stresses incidental to a fault at the machine terminals, at 110 per cent normal voltage. One Coulee generator has been so tested at 100 per cent voltage, and a very detailed inspection of the end turns thereafter failed to reveal a single loosened coil, wedge or support. In the case of the reference circuit-breaker tests in the left switchyard, a transformer bank of 10.8 per cent impedance and nearly a mile of transmission line is interposed between the machine terminals and the point of fault, thereby substantially cushioning the shock on the machine.

A direct comparison of the actual test values of the symmetrical component of short-circuit current obtained during the circuit-breaker fault tests involving a single isolated generator, referred to the low side of the transformer bank, with similar short-circuit currents obtained at the machine terminals during the generator acceptance tests of 1945, indicates that the presence of the transformer bank and line in the circuit reduces the short-circuit current in the machine by a ratio of 0.41 to 1. In other words, the fault current in the machine is only 41 per cent of what it would have been had the fault been applied directly to the machine terminals. Then since the magnetic forces involved are proportional to I^2, it follows that $0.41 \times 0.41 = 0.17$; or the

Figure 5. Ultimate Coulee 230-kv ring bus. Simplified schematic diagram


O.C.B.
SUB 5
SUB 1
RIVER
SNOHOMISH
FOSTER CREEK
SPOKANE
CLOSED
COVINGTON
COLUMBIA
MIDWAY
SUB 2
NO. BONNEVILLE
SUB 4
SUB 3
ENERGY
WOOD OR IRRIGATION PUMPS
ULTIMATE COULEE RING BUS 1-1-1-

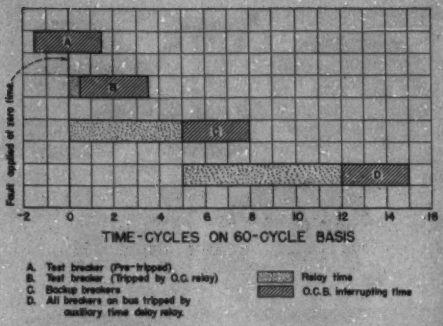


Figure 6. Illustrative chart showing approximate timing of the backup protection provided on all fault tests

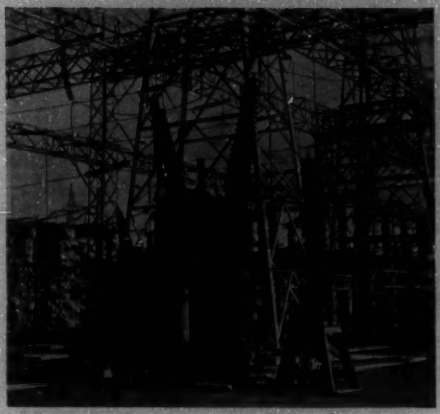


Figure 7. Type GM-3, 230-kv single-pole steel-tank oil circuit breaker

internal short-circuit stresses due to magnetic forces are only 17 per cent of what they would have been for faults directly on the machine terminals at 100 per cent voltage. Considering the fact that the machine is designed to withstand faults at 110 per cent voltage, and must necessarily contain a considerable safety factor, it becomes apparent that the developed magnetic stresses within the machine occasioned by circuit-breaker fault tests is extremely moderate in comparison with the machine design specifications. Such stresses should not constitute a hazard to the equipment unless the machine is in a very defective condition from other causes.

Backup Protection

Backup protection for the test circuit breaker was provided by the 3-cycle line circuit breakers and the associated line relays supplying the fault current. For the majority of the tests the backup circuit breakers were the two station circuit breakers on Midway Line 1. Either one or the other circuit breaker was used for the moderate capacity tests involving only half of the station; and both were used to parallel the busses for the high-capacity tests. It is noted that the 3,500,000-kva rating of the backup circuit breakers is not exceeded for faults up to somewhat above 7,000,000-kva, as on the full-capacity tests the fault current is divided approximately equally between the two backup circuit breakers, one on each bus; and, in addition, the backup circuit breaker operating time allows for a considerable current decrement before contact parting, further reducing the duty on the backup circuit breakers.

Backup protection and relaying was provided in the following operational sequence as illustrated in Figure 6:

1. The test circuit breaker was relayed by means of an instantaneous overcurrent relay, using a minimum current setting to obtain the fastest possible relay time. This relay tripped the test circuit breaker directly. In the case of pretripped tests, this relay acted as backup against failure of the pretripping function. If, for any reason, both the pretrip and the overcurrent trip had failed to trip the test circuit breaker or the test circuit breaker had failed to clear the fault promptly, the following backup operation would have been initiated:

2. The directional ground relay on the line supplying the fault current would pick up to trip its associated line circuit breakers. This same operation would take place at the far end of the line, thus completely clearing the test circuit breaker from all power sources. If however, either or both of the backup circuit breakers had failed to trip, further backup protection would have been initiated as follows:

3. The TD relay (time-delay bus-differen-

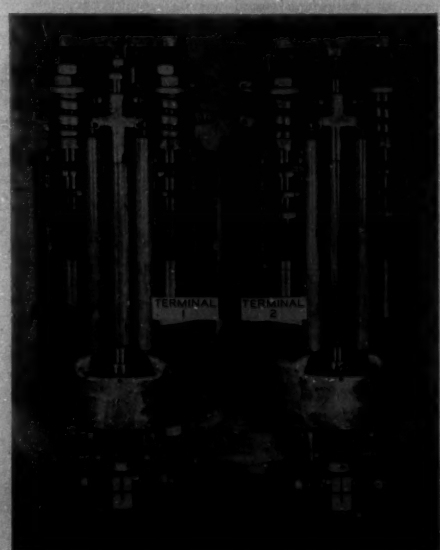


Figure 8. Pair of multiflow deion interrupters removed from the Type GM-3 oil circuit breaker after completion of successful fault interruptions up to 7,500,000 kva

Figure 9. View of contact tips taken from the *Type GM-3* oil circuit breaker after completion of high-capacity fault tests up to 7,500,000 kva

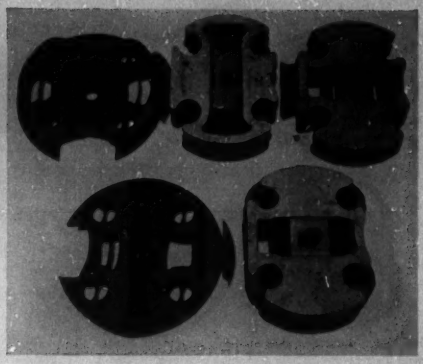


Figure 10. Fibre plates removed from one of the deion interrupter units shown in Figure 8, after completion of the tests

tial trip) would have closed its contacts after approximately 7-cycles delay after receiving its tripping impulse and operate to clear all other circuit breakers from the station bus, thus removing all sources of power.

In order to prevent undesirable high-speed tripping of the backup circuit breakers when not required for protection, the first zone tripping contacts on the *Type GCX* distance line relays and the instantaneous contacts on the directional ground line relay were "papered-out." The second and third zones of the *Type GCX* relays and the inverse time contacts on the ground relay remained operable for the desired delayed backup protection. An oscillograph record was obtained of the trip current to the backup circuit breakers to provide a positive indication as to whether a test circuit breaker had cleared first in cases where the backup circuit breakers had operated during a test. The timing sequence shown in Figure 6 is only typical. Actual timing was varied slightly as particular test conditions dictated.

Current Measurements

Since all faults were single-line-to-ground, it was possible to measure fault currents by means of a 5,000/1 ampere (1-ampere secondary rating) through-type current transformer located in the connection to the grounded pole of the test circuit breaker. Maximum asymmetrical fault currents encountered during the tests did not exceed four times the nominal continuous rating, which was well within the acceptable range of accuracy. The secondary circuit of this transformer was used to operate one element of the 12-element laboratory oscillograph, for recording instantaneous values of fault current. High accuracy type bushing current transformers, located in the various steel tank type circuit breakers, were used in conjunction with the laboratory oscillograph to record instantaneous values of line-charging current during the line-switching tests. Current transformers of the 230-kv wound-type with a ratio of 300/1 ampere (1-ampere secondary rating) were used for measuring the line-charging current for the line-switching tests on the *Type FG-25* impulse circuit breaker.

Voltage Measurements

Instantaneous line and bus voltages were obtained and recorded on the magnetic oscillograph by two distinct and separate methods:

1. By means of a conventional capacitance potential device.
2. By means of a coupling capacitor connected as a capacitance potential divider, with the transformer removed, and used in conjunction with a specially designed electronic amplifier and magnetic oscillograph.

The latter proved to be the more accurate and superior method as it faithfully indicated d-c potentials trapped on line conductors as well as a-c transient voltages. The electronic amplifier was necessarily required when recording instantaneous voltages on the magnetic oscillograph because of the relatively large amount of power required to drive the vibrator. This very satisfactory and relatively inexpensive method of faithfully recording high frequency transient voltages was recently described in a paper[4] which presented the basic method and similar applications in detail.

Cathode-ray oscillograph (CRO) equipment was furnished and operated by both manufacturers to supplement and monitor other desired voltage measurements. For recording 230-kv line and bus potentials a coupling capacitor without transformer was connected directly to the deflection plates of the CRO tube, after having been previously modified by replacement of the bottom auxiliary capacitor unit by one with a capacity of about 2.5-microfarad. This modification re-

Figure 11. Arcing fault applied by dropping a lead weight and steel music wire from the upper grounded conductor to the lower energized conductor

Table I. Summary Three-Phase Line Switching Tests

230-Kv Type GO-2 Tank Circuit Breaker

	Series "B" Isolated				Series "O" Isolated			
	A-φ	A-φ	B-φ	C-φ	A-φ	A-φ	B-φ	C-φ
Number line-dropping tests	15	15	30	30	15	14	29	29
Miles line	182*	82	82	82	182*	82	82	82
Line voltage kv	241	241	241	241	241	241	241	241
Per cent with single restrikes	0	0	0	0	7	14	14	14
Per cent with multiple restrikes	0	0	0	0	0	0	0	3
Per cent with no restrikes	100	100	100	100	93	86	86	83
Minimum interrupting time cycles	6.9	8.3	7.1	6.5	2.4	2.4	2.4	2.4
Average interrupting time cycles	8.2	9.0	9.6	8.3	2.7	2.7	2.7	2.7
Maximum interrupting time cycles	9.9	10.8	11.2	9.6	3.0	2.9	3.0	3.4
Maximum lift rod travel at interruption (per cent open)	81	90	93	78	8	8	9	
Minimum lift rod travel at interruption (per cent open)	48	74	52	44		4	4	4
Maximum overvoltage across circuit breaker (times normal)**				0.95			1.4	

* C-φ of 100-mile line in parallel with 82-mile line. Equivalent to 182 mile line.

** "Normal" voltage across the circuit breaker is assumed as follows: At the instant of arc interruption, the crest value of voltage (138×1.414=196 kv) is left trapped upon the line, and 1/2 cycle later the bus has reached crest value, also 196-kv, but of opposite polarity. The vector difference of these two crest values, or 392-kv will therefore normally appear across the circuit breaker pole unit. The figures in the tabulation are multiples of this theoretical normal as obtained from the magnetic oscillograms

duced the available potential across the auxiliary capacitor to a safe value of approximately 60 volts. The potential appearing across the auxiliary capacitor was conducted to the CRO deflection plates by means of a 52-ohm coaxial cable. This arrangement constituted a true series capacitance potential divider circuit, linearily responsive to voltage and practically independent of frequency without phase angle error, within the range involved. The foregoing arrangement was used during various portions of the test series to record line-to-ground voltage; bus-to-ground voltage; and line-to-bus voltage; the latter indicating the voltage directly across a circuit breaker pole unit during line switching tests. This was considered of particular value as it was recognized that transient overvoltages within themselves can be objectionable, but the most objectionable situation which can occur, as far as the performance of the circuit breaker interruptors is concerned, is the condition wherein simultaneous over-voltages appear on both sides of the circuit breaker 180 degrees out of phase. The vector sum of such potentials could build up to very high values across the interruptor units, hence the particular interest in this type of measurement during the field tests.

Description and Results of Tests

Line Switching Tests Series "B"—*Type GO-2*

This series consisted of a group of 35 line-charging and line-dropping operations on a *Type GO-2* tank circuit breaker, after special 1,250-ohm wire-wound resistors had been installed across each interruptor for this specific test. This circuit breaker was installed and placed in operation in 1944. Table I summarizes the pertinent data obtained during this series of line-dropping tests. It is noted that there were no restrikes on any of the 30 3-phase tests (equivalent to 90 single-phase tests). The crest magnitude of the transient voltages during switching were very moderate and caused no concern. However, the excessively long interrupting time (resistor current) may cause difficulty, particularly on the unfaulted phases, in installations where high-speed reclosing operations are employed. The results also indicate that the circuit breaker was 93 per cent open on one test before the resistor current was inter-

Figure 12. Typical oscillogram of line-switching test "O" *Type* GO-2 tank circuit breaker

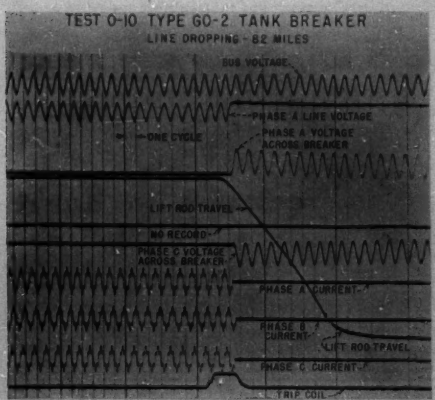


Figure 13. Typical oscillogram of single-phase fault test "D" *Type* GM-3 tank circuit breaker

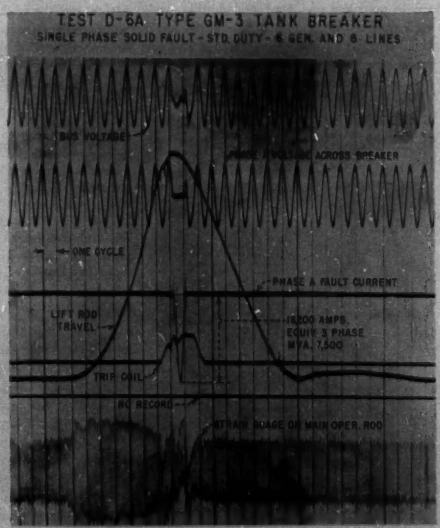


Figure 14 (left). *Type FK-439* oil circuit breaker temporarily mounted in the test position

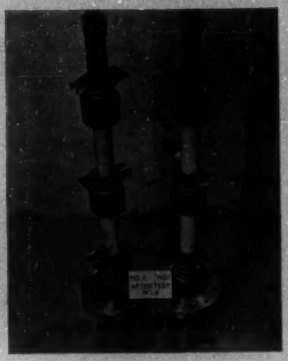


Figure 16. Movable contacts of *Type FK-439* oil circuit breaker after interrupting a series of equivalent 3-phase faults up to nearly 7,000,000 kva

rupted. The difference between 93 and 100 per cent, or the fully open position, appears to provide insufficient margin of safety.

LINE SWITCHING TESTS SERIES "O"—*Type GO-2*

After completion of the foregoing test Series "B," the special 1,250-ohm resistors were removed, the original 0.4 megohm resistors replaced and the circuit breaker restored to normal. This was followed by an identical test series, using the same bus voltage, line lengths and switching arrangement. Table I summarizes the test results of this series and Figure 12 shows a typical oscillogram. It is noted that 14 per cent of the tests had single restrikes and that one double restrike occurred on *Test 0-31*. However, these single and double restrikes are of a secondary importance as the magnitudes of the overvoltages resulting therefrom were not objectionable. This observation is also borne out by the absence of objectionable overvoltages from the cathode-ray records obtained on the far end of the transmission lines being dropped. A greater margin was provided for arc interruption than was provided by the low ohmic value resistors of Series "B," as evidenced by the fact that the maximum lift-rod travel was only 9 per cent of the total opening stroke at the point of current interruption. Based on a normal 18-inch stroke, this corresponds to approximately $1^4/_8$ inches of lift-rod travel. In general, it appears that the high ohmic value resistors (0.4 megohm per interruptor) are preferable to the low ohmic value resistors, even though they do not completely eliminate restrikes. No serious overvoltages were obtained by either the high or low ohmic value resistors, but the high ohmic value resistors permitted arc extinction within the specification time of 3 cycles.

An internal inspection of the circuit breaker after completion of each series of line-dropping tests revealed a minimum of contact pitting, carbonization, or oil discoloration. The appearance of the interruptors and the mechanical adjustments of the circuit breaker was completely satisfactory throughout.

FAULT TESTS SERIES "D"—*Type GM-3*

This series consisted of a group of nine single-phase-to-ground faults, using the standard AIEE duty-cycle of two close-open operations separated by a 15-second time interval. Two of the nine tests were with arcing faults. All fault tests were conducted on a special single-pole pneumatically-operated 230/196-kv tank *Type GM-3* oil circuit breaker, shipped to Grand Coulee specifically for this purpose. Table II lists the fault currents, interrupted kilovolt-ampere and other pertinent information. Figure 13 shows a typical oscillogram. The maximum total

Figure 15 (left). Looking through the manhole into the tank of the *Type FK-439* oil circuit breaker. View shows the two interrupters, lift rod, and movable crosshead

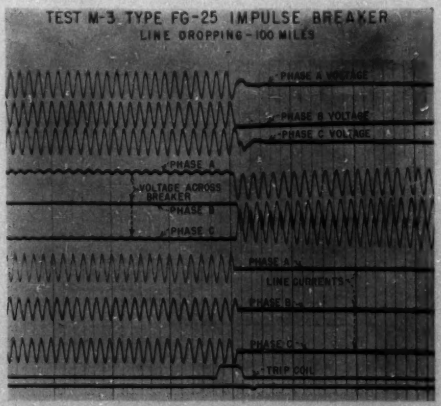


Figure 17. Typical oscillogram of line-switching test "M" *Type FG-25* impulse circuit breaker

Table II. Grand Coulee Single-Phase Fault Tests

230-Kv Type GM-3 Tank Circuit Breaker
D Test March 9, 1947 & F Test March 16, 1947

Test Number†	Bussing Arrangement	Ground Fault	Duty Cycle	Interr. Time Cycles	Kilovolts	A-C Component	D-C Component	Total Asym. Amps	Equiv. 3-φ Megavolt-Ampere
D-1-A	1 Gen.	Solid	Std*	2.7	241	1,050	1,005	1,452	607
D-1-B	1 Gen.	Solid		2.5	241	1,050	1,005	1,452	607
D-1-AR	1 Gen.	Solid	CO		241		(No current record)		
D-2-A	1 Gen.	Arc	Std*	2.5	241	1,105	727	1,323	554
D-2-B	1 Gen.	Arc			241		(No record)		
D-3-A	2 Gen.	Solid	CO		241		(No trip record)		
D-3-AR	2 Gen.	Solid	CO	2.6	241	2,440	Neg.	2,440	1,020
D-4-A	2 Gen.	Arc	Std*	2.5	241	2,425	1,207	2,710	1,134
D-4-B	2 Gen.	Arc		2.6	241	2,440	735	2,550	1,065
D-5-A	3 G & 3 L.	Solid	Std*	2.4	238	6,940	1,895	7,200	2,970
D-5-B	3 G & 3 L.	Solid		2.6	238	6,880	4,550	8,240	3,400
D-5-AR	3 G & 3 L.	Solid	Std*	2.2	238	6,800	Neg.	6,800	2,800
D-5-BR	3 G & 3 L.	Solid		2.8	238	6,800	3,570	7,840	3,230
D-6-A	6 G & 6 L.	Solid**	Std*	1.8	238	11,000	14,500	18,200	7,500[x]
D-6-B	6 G & 6 L.	Solid**		2.6	238	11,000	14,000	17,800	7,340
Complete Circuit Breaker Overhaul—Installed All New Contacts									
F-8-A	1 Gen.	Solid**	Rec[xx]		241		(No current record)		
F-8-B	1 Gen.	Solid**			241		(No current record)		
F-9-A	1 Gen.	Solid**	Rec[xo]	2.6	241	1,170	740	1,200	501
F-9-B	1 Gen.	Solid**		2.7	241	1,150	433	1,160	485
F-1-A	3 G & 3 L.	Solid**	Rec[xx]	2.4	238	7,050	Neg.	7,050	2,910
F-1-B	3 G & 3 L.	Solid**		2.7	238	7,050	4,730	8,500	3,510
F-2-A	3 G & 3 L.	Solid**	Rec[xo]	2.6	238	7,100	4,950	8,650	3,560
F-2-B	3 G & 3 L.	Solid**		2.6	238	7,150	1,770	7,350	3,025
F-3-A	6 G & 6 L.	Solid**	Rec[xx]	2.3	238	10,250	1,485	10,350	4,270
F-3-B	6 G & 6 L.	Solid**		2.1	238	10,150	10,450	14,550	6,000
F-4-A	6 G & 6 L.	Solid**	Rec[xo]	2.3	238	10,200	3,680	10,850	4,470
F-4-B	6 G & 6 L.	Solid**		2.6	238	10,250	5,660	11,700	4,830
F-5-A	6 G & 6 L.	Solid**	Rec[xo]	2.2	238	10,100	2,970	10,500	4,330
F-5-B	6 G & 6 L.	Solid**		2.5	238	10,500	6,650	12,450	5,130
Internal Inspection—Installed All New Contacts									
F-6-A	3 G & 3 L.	Arc	O-18C-C	2.2	238	5,330	2,335	6,750	2,790
F-7-A	6 G & 6 L.	Arc	O-18C-C	2.2	238	10,000	2,400	10,300	4,250
F-10-A	6 G & 6 L.	Arc	O-18C-C	2.5	238	9,900	Neg.	9,900	4,080
F-11-A	6 G & 6 L.	Arc	O-15C-C	2.1	238	9,900	2,120	10,100	4,150
F-12-A	6 G & 6 L.	Arc	O-13C-C	2.6	238	9,950	3,820	10,800	4,330
F-13-A	1 Gen.	Arc	O-12C-C	3.0	241	1,110	240	1,140	476
F-14-A	1 Gen.	Arc	O-12C-C	3.0	241	1,100	510	1,210	505

* CO-15SEC-CO

** Tests which were pretripped

[x] Single current loop

[xx] O-80C-CO

[xo] O-18C-CO

† Test D-1-A is initial fault and Test D-1-B is fault after reclosure on same test. R indicates repeat of original test.

Notes: Arc did not re-ignite after circuit breaker reclosure on any arcing faults. Contact parting time 1.2-cycles for all tests except 1.1-cycles for Test D-6-A and B.

asymmetrical fault current interrupted in this test Series "D" was 18,200 amperes, corresponding to an equivalent 3-phase fault of 7,500,000 kva. No undue external indications of distress were apparent until the final full capacity *Test D-6-B*. On this test, the second half of the duty-cycle, a small puff of gas was noted to escape from the circuit breaker vent pipe, and the circuit breaker tank exhibited a visible jump which was accompanied by a light ground shock. After this test it was noted that the nuts on the four 1-inch anchor bolts could be turned down 1/8 to 1/4 inch, due to the loosening of these bolts in the foundation.

All interruptions were successful and were accomplished within the specification time of 3 cycles. A subsequent inspection of the contacts indicated that they were still in serviceable condition. Oil samples tested satisfactorily for dielectric strength.

Fault Test Series "F"—*Type GM-3*

After conclusion of fault test Series "D" the test circuit breaker was completely checked and overhauled and a complete set of new contacts installed. As reassembled for Series "F", the condition of the circuit breaker was equivalent to that of new equipment. Five additional 1-inch anchor bolts were installed around the circumference of the circuit breaker tank to assist the four original bolts which had been loosened during test Series "D." Test Series "F" was conducted in a similar manner to Series "D" except that "60-cycle" and faster reclosures were used to observe and determine the behavior, operating ability and possible signs of distress under reclosing conditions, for both solid and arcing faults. An adjustable electronic timer was provided for accurately timing the reclosing operation. This timer proved highly satisfactory. Test Series "F" was accomplished in two groups. The first group, consisting of *Tests 1–7* inclusive, was followed by a complete circuit breaker inspection and overhaul. A complete set of new contacts was installed, followed by the second group, consisting of *Tests 8–14* inclusive. The first group consisted of solid-ground faults and the second group consisted of arcing-ground faults. Inspection of the circuit breaker after each group of tests revealed all contacts and interruptors in a serviceable condition. All interruptions were successful and were well within the specification limit for interrupting time. Table II lists the fault current values, the equivalent of 3-phase megavolt-ampere and other pertinent data. The heaviest solid fault interrupted was 6,000 megavolt-ampere on *Test F-5-B*, and the heaviest arcing fault interrupted was 4,330 megavolt-ampere on *Test F-12*. The test circuit breaker was pretripped on the solid faults but not on the arcing faults, which accounts for the difference in interrupted megavolt-ampere between the two groups.

For the first group, *Tests 1–7* inclusive consisting of "O-CO" duty cycles on a solid ground fault, it was necessary to start with the test circuit breaker in the closed position and apply the fault with a station circuit breaker. All three poles were connected in parallel to distribute

Table III. Summary Three-Phase Line Switching Tests

230-Kv Type FG-25 Impulse Circuit Breaker

	Series "M" Test Number	Miles Line	Interrupting Time Cycles
On System	M-1	100	2.25
	M-2	100	2.20
	M-3	100	2.20
	M-4	100	2.35
	M-6	182	2.20
	M-7	182	2.35
	M-8	182	2.20
	M-9	182	2.30
	M-11	264	2.20
	M-12	264	2.20
	M-13	264	2.30
	M-14	264	2.30
Isolated	M-16	364	2.50
	M-17	364	2.55
	M-18	364	2.20
	M-21	364	2.40
	M-22	182	2.55
	M-23	182	2.60
	M-24	182	2.45
	M-27	100	2.50
	Average		2.3

The above tabulated values of interrupting time include the circuit breaker contact-parting time of 1.9 cycles. All tests at 241-Kv.

No restrikes were obtained on any of the above tests.

Table IV. Grand Coulee Single-Phase Fault Tests

230-Kv Type FG-25 Impulse Circuit Breaker—March 23, 1947

Test Number	Bussing Arrangement	Duty Cycle	Interr. Time Cycles	Kilo-volts	A-C Com-ponent	D-C Com-ponent	Total Asym. Amps	Equiv. 3/4 Megavolt-Amperes
H-1	1 Gen.	O	2.7	241	1,080	568	1,220	508
H-1-B1	1 Gen.	O	2.6	150	640	302	710	184
H-1-B2	1 Gen.	O	2.9	241	1,075	462	1,170	489
H-3	2 G & 3 L	O	2.7	237	5,420	820	5,480	2,250
H-3	2 G & 3 L	CO	2.7	247	5,450	3,320	6,380	2,620
H-4	4 G & 3 L	O	2.7	237	7,500	2,960	8,070	3,310
H-5-A	4 G & 3 L	Std*	2.9	237	7,200	3,680	8,080	3,320
H-5-B	4 G & 3 L		2.6	237	7,420	2,330	7,780	3,190
H-6	4 G & 6 L	O	2.9	237	7,780	2,860	8,280	3,400
H-7	4 G & 6 L	CO	2.9	237	7,920	3,260	8,560	3,510
H-8	6 G & 6 L	O	2.7	237	10,100	6,220	11,850	4,865
H-9	6 G & 6 L	CO	2.7	237	10,100	Neg	10,100	4,140
H-10	6 G & 6 L	CO	2.7	237	10,250	7,070	12,450	5,110
H-11-A	6 G & 6 L	Std*	2.8	237	10,400	7,630	12,900	5,295
H-11-B	6 G & 6 L		2.9	237	10,600	11,600	15,700	6,445
H-12-A	6 G & 6 L	Std*	3.0	237	10,400	12,730	16,450	6,750
H-12-B	6 G & 6 L		3.0	237	10,350	7,720	12,900	5,295

* CO-15SEC-CO

Note: Contact parting time 1.8-cycles for all tests. All faults solid single-phase-to-ground.

the high fault current between the pole units, and the circuit breaker was placed between the test circuit breaker and ground. On the arcing-fault *Test 8–14*, inclusive, the reclosing time was successively reduced on each test to observe whether the arc would reignite after the initial interruption and high speed reclosure. Although the reclosing time was reduced to 12 cycles as indicated in Table II, the arc was sufficiently deionized in all cases to prevent reignition after reclosure. Wind velocity was considered to be zero during these deionization tests. *Tests 13–14* were run with a single unit on an isolated bus in order to include a wide range of fault current values, to substantiate the assumption that the arc would not reignite after faults of both high and low current magnitudes. All reclosures were successful.

Line Switching Tests Series "M"—*Type FG-25*

This series consisted of a group of 27 3-phase line switching operations on the *Type FG-25* impulse circuit breaker. This circuit breaker was installed and placed in operation in 1945. Table III summarizes the test results of this series and Figure 21 shows a typical oscillogram. Of the 27 3-phase tests, 7 were line-charging operations. On the balance of 20 3-phase line-dropping tests the average interrupting time was 2.32 cycles, of which 1.9 cycles represents the circuit breaker contact-parting time. In other words, the average arcing time was 0.42 cycle, or less than one-half cycle. The maximum interrupting time of any test of the series was 2.6 cycles and the minimum time was 2.1 cycles. There were no restrikes on any test and no indications of switching overvoltages on any of the records, regardless of the fact that the tests were conducted on a wide variety of line lengths and switching conditions. In conclusion, it may be stated that on the basis of test Series "M," the impulse

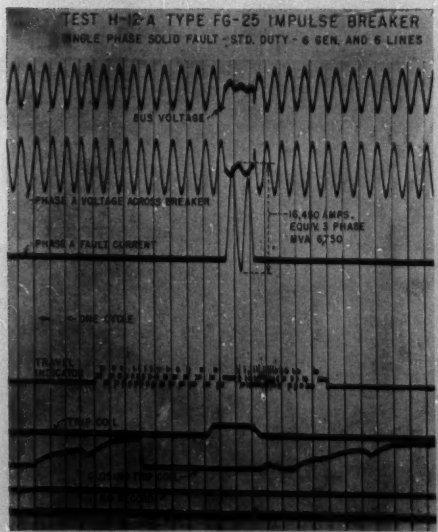


Figure 18. Typical oscillogram of single-phase fault test "H" *Type FG-25* impulse circuit breaker

Figure 19. 3,500,000-kva 3-cycle oil-poor impulse *Type FG-25* circuit breaker, permanently installed on 230-kv Coulee-Midway Line 1

Figure 20. Enlarged view of one of the five contacts from A-phase pole unit of the Type FG-25 impulse circuit breaker, after interruption of a series of equivalent 3-phase faults up to nearly 7,000,000-kva

circuit breaker exhibited an excellent capability for switching long, unloaded high-voltage lines, without restrikes, overvoltages or prolonged arcing. The extremely short duration of the arcing time prolongs contact life and reduces oil carbonization.

Fault Tests Series "H"—*Type FG-25*

This series consisted of 14 single-phase-to-ground faults. Table IV lists a summarized tabulation of fault currents and the equivalent 3-phase megavolt-amperes interrupted on each test. Figure 22 shows a typical oscillogram. This test series of 14 high-capacity fault-clearing operations might well have been considered a life test as well as an interrupting capacity test, due to the large number of interruptions without the benefit of internal inspections. The majority of these operations were at an interrupting duty of approximately double the nominal circuit breaker rating of 3,500,000 kva.

At the conclusion of the 14 high-capacity interrupting duty tests, the contact board was removed and the contacts inspected. All contacts were still in serviceable condition. Only one contact of the ten showed definite signs of distress, where one corner of the Elkonite contact tip had been undercut by the arc, but even this contact was still in a serviceable condition. There was little question but what the circuit breaker could have remained in normal line service and successfully carried load current without excessive overheating. Several fiber studs had failed. These failures were not considered serious in relation to the present series of tests, as the failures were such as to not interfere with the proper operation of the interrupter. The dielectric strength of the insulating oil was also satisfactory after the tests. All interruptions were entirely successful and were accomplished within the nominal 3-cycle interrupting-time rating of the circuit breaker. The average interrupting-time for all tests was 2.8 cycles, with a minimum of 2.6 cycles and a maximum of 3.0 cycles within the series. The circuit breaker demonstrated an excellent capability for repeatedly interrupting fault currents up to almost double its nominal rated capacity.

Line Switching Tests Series "P"—*Type FK-439*

This test series consisted of a complete group of 40 single-phase tests, and included 20 tests wherein the line was switched from a loaded bus, and 20 tests wherein the line was switched from an isolated bus supplied by two isolated generators. Four different line lengths were used to obtain the effects of several magnitudes of line-charging current, as follows:

1. A single conductor of Midway Line 1 with the other two conductors grounded, equivalent to about 100 miles of line;
2. Two conductors in parallel with the third conductor grounded, equivalent to about 200 miles of line;
3. Three conductors in parallel, equivalent to about 300 miles of line;
4. One conductor of Spokane Line 3 in parallel with three conductors of Midway Line 1, equivalent to about 382 miles of line.

The actual charging current in the latter case was 212 amperes at 233-kv, or 28,500 charging kva. The average interrupting time of all line-dropping interruptions was 5.45 cycles, with a maximum single test of

Figure 21. Typical oscillogram of line-switching test "P" Type FK-439 tank circuit breaker

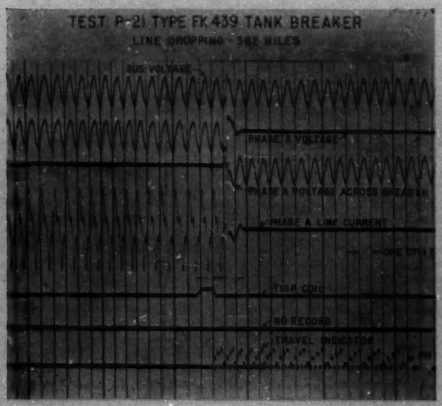


Figure 22. Typical oscillogram of single-phase fault test "L" Type FK-439 tank circuit breaker

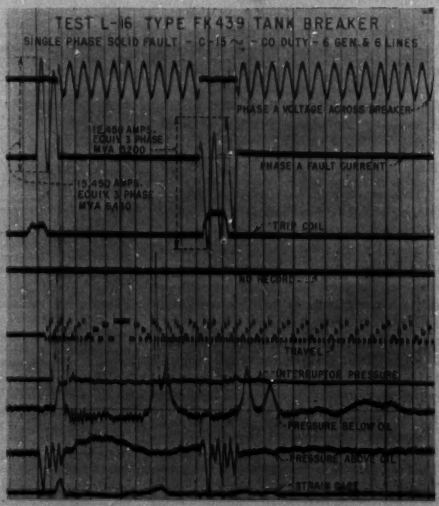


Table V. Summary Single-Phase Line-Switching Tests

230-Kv Type FK-439 Tank Circuit Breaker

	Series "P" on System				Series "P" Isolated			
Number line-dropping tests	4	4	6	4	4	4	4	4
Miles single-phase line	100	200	300	382	382	300	200	100
Charging amperes RMS	63	120	152	212	236	162	122	68
Line voltage kv	233	233	233	233	241	241	241	241
Number tests with single restrikes	0	0	4	2	3	3	0	0
Number tests with multiple restrikes	0	0	0	0	0	0	0	0
Number tests with no restrikes	4	4	2	2	1	1	4	4
Minimum interrupting time cycles	3.3	5.1	3.8*	3.6*	3.9*	4.0*	4.1	6.5
Average interrupting time cycles	5.3	5.9	5.4	4.8	5.0	5.0	5.3	6.9
Maximum interrupting time cycles	6.5	7.4	6.9	5.9	6.1	6.5*	6.9	7.4

* Restrike obtained on this test

Average interrupting time of all tests from loaded bus, 233-kv ..... 5.4 cycles

Average interrupting time of all tests isolated from system, 241-kv ..... 5.4 cycles

Per cent of tests with restrikes from loaded bus ..... 33 per cent

Per cent of tests with restrikes isolated from system ..... 37 per cent

7.4 cycles and a minimum single test of 3.3 cycles. Table V summarizes the test results and Figure 17 shows a typical oscillogram. The pole-unit 1,200-ohm resistors were checked for continuity with an ohmmeter several times during the tests and at the conclusion of the complete test series, and were found to be satisfactory in every case. Single restrikes were obtained on 35 per cent of the tests but resulting overvoltages were only moderate. No multiple restrikes were obtained on any tests. No restrikes were obtained when dropping either 100 miles or 200 miles of conductor, corresponding to charging currents up to 122 amperes at 241 kv. However, when dropping 300 miles or 382 miles of conductor, corresponding to charging currents of between 152 amperes and 212 amperes, single restrikes were obtained on 63 per cent of the tests. The excessively long resistor current of 7.4 cycles would probably influence the feasibility of using this circuit breaker for high-speed reclosing duty, particularly if reclosing is attempted in less than 20 cycles.

Fault Tests Series "L"—*Type FK-439*

This test series, consisted of a total of 24 single-phase-to-ground faults on pole (1) of the *Type FK-439* oil circuit breaker. All faults were solid-ground on *A*-phase only, using "open," "close-open," and reclosing operations. Table VI lists a summarized tabulation of fault currents, equivalent 3-phase megavolt-amperes interrupted on each test, and other test data. Figure 18 shows a typical oscillogram. This test was performed in two major groups:

1. *Tests 1 to 9R*, inclusive using "open," "close-open" and standard 15-second duty-cycle operations,
2. *Tests 10 to 19*, inclusive, using high-speed reclosing operations and two additional standard duty-cycles.

After the conclusion of the first group, the oil was removed from the tank and a complete new set of contacts installed before resumption of the test series. In both cases the contacts appeared to be in serviceable condition, with the possible exception of one contact from the second group which had lost a small portion of its Elkonite tip. This contact would undoubtedly have still carried load current successfully but probably would not have withstood many additional full system fault tests. However, considering the excessively large number of high-capacity operations to which these contacts had been subjected, they were found to be in good condition. This *Type FK-439* circuit breaker successfully interrupted a large number of high capacity faults, up to and including 6,800-megavolt-amperes, with no apparent distress. The dielectric strength of the oil was satisfactory after the conclusion of the tests.

Conclusions

The successful performance of all the 230-kv oil circuit breakers at interrupting capacities approaching 200 per cent of

Table VI. Grand Coulee Single-Phase Fault Tests

230-Kv Type FK-439 Tank Circuit Breaker—April 6, 1947

Test Number	Bussing Arrangement	Duty Cycle	Interr. Time Cycles	Kilovolts	A-C Component	D-C Component	Total Asym. Amps	Equiv. 3-φ Megavolt-Amperes
L-1	1 Gen	O	2.2	241	920	1,020	1,375	573
L-2	2 G & 3 L	O	2.4	238	5,600	3,250	6,470	2,660
L-3	2 G & 3 L	CO	2.6	238	5,600	Neg.	5,600	2,300
L-4	4 G & 6 L	O	2.1	238	8,000	Neg.	8,000	3,290
L-5	4 G & 6 L	CO	2.5	238	7,900	6,500	10,250	4,220
L-5-A	4 G & 6 L	Std*	2.5	238	8,300	6,080	10,300	4,245
L-5-B	4 G & 6 L			238		(No current trace)		
L-6	6 G & 6 L	O	2.3	238	10,100	5,520	11,500	4,740
L-7	6 G & 6 L	CO	2.3	238	10,300	Neg.	10,300	4,245
L-8	6 G & 6 L	CO	2.7**	238	10,300	3,820	10,980	4,525
L-9-A	6 G & 6 L	Std*	2.5**	238	10,500	8,070	13,250	5,460
L-9-B	6 G & 6 L		2.8**	238	10,400	6,500	12,260	5,050
L-9-AR	6 G & 6 L	Std*	2.3	238	10,200	9,050	13,640	5,625
L-9-BR	6 G & 6 L		2.6	238	10,300	5,800	11,820	4,870
Internal Inspection—Installed All New Contacts								
L-10	2 G & 3 L	O	2.5	238	5,900	2,690	6,480	2,670
L-10-AR	2 G & 3 L	Rec*	2.5	241	5,850	Neg.	5,850	2,440
L-10-BR	2 G & 3 L		3.1	241	5,800	2,270	6,220	2,600
L-10-R2	2 G & 3 L	O	2.5	241	5,700	2,970	6,430	2,680
L-11-A	4 G & 6 L	Rec*	2.3	241	8,200	6,800	10,650	4,445
L-11-B	4 G & 6 L		2.6	241	8,200	2,545	8,570	3,580
L-12-A	6 G & 6 L	Rec*	2.2**	241	10,800	Neg.	10,800	4,510
L-12-B	6 G & 6 L		2.5	241	10,500	9,470	14,140	5,900
L-13-A	6 G & 6 L	Rec*	2.0**	241	10,400	4,800	11,460	4,785
L-13-B	6 G & 6 L		2.4	241	10,300	6,640	12,250	5,115
L-14-A	6 G & 6 L	Rec**		241		(No current trace)		
L-14-B	6 G & 6 L			241		(No current trace)		
L-15-A	6 G & 6 L	Rec**	2.0**	241	10,250	4,470	11,200	4,675
L-15-B	6 G & 6 L		2.5	241	10,250	9,540	14,000	5,845
L-16-A	6 G & 6 L	Rec**	2.4**	241	10,200	11,600	15,450	6,450
L-16-B	6 G & 6 L		2.6	241	10,700	6,360	12,450	5,200
L-17-A	6 G & 6 L	Rec**	2.4**	241	10,300	Neg.	10,300	4,300
L-17-B	6 G & 6 L		2.5	241	10,000	9,475	13,770	5,750
Changed Bus Arrangement								
L-18-A	6 G & 6 L	Std*	2.6**	241	10,700	8,070	13,400	5,595
L-18-B	6 G & 6 L		2.7**	241	10,500	12,450	16,290	6,800
L-19-A	6 G & 6 L	Std*	2.9**	241	10,500	3,250	10,980	4,583
L-19-B	6 G & 6 L		2.8**	241	10,500	Neg.	10,500	4,380

* CO-15SEC-CO

** Tests which were pretripped

* O-20C-CO

** O-15C-CO

** O-12C-CO

Note: Contact parting time 1.4-cycles for all tests except 1.7-cycles for Tests L-8, L-9-A and B, L-18-A and B, and L-19-A and B. All faults solid single-phase-to-ground.

their nameplate rating was remarkable and gratifying. The confidence thus gained from these field tests in the design and performance of modern high-voltage circuit breakers will permit continued interconnection of our high-voltage power systems without fear of being limited by circuit breaker interrupting capacity or performance. After review and study of these tests the following conclusions were reached:

1. The many successful interruptions of 6,000,000-kva and more at 230-kv clearly indicate that 10,000,000-kva interrupting capacity 230-kv circuit breakers can be developed immediately and that even higher capacity circuit breakers are attainable.

2. From the reclosing performance of the circuit breakers it appears that no de-rating factor is required for one 20-cycle reclosure.

3. Successful performance of the circuit breakers at 12-cycle reclosing operations without reignition of the arcing faults indicates that faster reclosing speeds than 20 cycles are attainable.

4. The impulse-type stored-energy circuit breaker demonstrated superior performance during interruption of line-charging currents and fault currents.

5. The use of low ohmic value resistors in parallel with the interruptor units in tank-type circuit breakers permits flow of resistor current long enough to question performance on less than 20-cycle reclosures.

6. It has been conclusively demonstrated that field tests involving short-circuit values of 7,500,000-kva can be safely performed on an extensive interconnected power system without mishap, hazard to equipment or serious operating disturbances or delays in prearranged test schedules.

7. Such field tests are essential to permit the continued development of high-voltage transmission systems and equipment. High power laboratory tests would be inconclusive without field tests at actual system voltages to substantiate laboratory data. Valuable information is also gained on the performance of the transmission system under fault conditions. Without performance of these tests the immediate development of 10,000,000-kva interrupting capacity 230-kv circuit breakers would not have been possible.

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13. A 230-Kv, Three-Cycle Oil Circuit Breaker for Extra-Heavy Arc Rupturing Duty, W. M. Leeds, G. B. Cushing. AIEE Transactions, volume 67, 1948, pages 236–42.

14. Unusual Performance of Standard 230-Kv Impulse and Tank Type Oil Circuit Breakers on Field Tests, E. B. Rietz. AIEE Transactions, volume 67, 1948, pages 267–70.

Discussion

Philip Sporn and **H. P. St. Clair** (American Gas and Electric Service Corporation, New York, N. Y.): It is gratifying to see once again a high capacity high-voltage system made available for carrying out a circuit breaker testing program, and it is particularly gratifying to see the material advance in circuit breaker testing experience that has been accomplished by the tests described in the paper. Besides presenting the test results themselves, the authors have done well in our opinion to discuss the relative hazards of staged faults as compared with the normal hazard of lightning, and so forth, and to point out that these tests, as severe as they are, do not impose on generators and other equipment nearly as great a shock as their designs are intended to withstand. As a corollary, we believe they could well have added that if such tests had disclosed weaknesses, even resulting in failures, it would be better to find them out during carefully observed test procedures than during normal operation, particularly during critical peak load periods. We should like also to express the hope that the complete success of this program in its adherence to schedule and absence of unfavorable incidents, experiences which we have repeatedly checked on one system, will give courage and inducement to other systems to lend their facilities to the making of field tests which are so vitally important to the advancement of the circuit breaker art.

The results of these tests are of double value, including as they do, both no-load line switching tests, and high capacity short circuit tests. It is most significant and timely, we believe, that these tests have thoroughly explored the line-switching ability or characteristics of three types of circuit breakers, two of the conventional tank type using resistors to control overvoltages and restrikes, and one of a strictly stored-energy type, which, of course, requires no resistors.

As pointed out in a discussion we presented a year ago in connection with a paper by Leeds and Van Sickle,[1] the complete elimination of transient over-voltages due to switching on transmission systems may open up new possibilities for economies in the entire field of high voltage transmission. Substantially lower levels of insulation along the lines suggested in the recent paper by Sporn and Monteith[2] are believed to be feasible with respect to lightning and arrester co-ordination, but not without eliminating the more destructive effects of switching transients with their high energy content as compared with lightning transients.

While these tests seemed to demonstrate that the use of properly selected values of resistance across the contacts of conventional oil circuit breakers will limit the number of restrikes and prevent resulting overvoltages from reaching dangerous values, it was also shown that the resistors themselves may introduce undesirable effects, such as lengthening of the arc. It is clearly necessary to strike a balance or compromise between a resistance value low enough to control restriking effectively, but not low enough to cause excessive arc length which, of course, would interfere with successful ultra-rapid reclosing. Notwithstanding the confidence expressed by the designers that the right amount of resistance can be found to control both of these opposing effects within safe limits, if there is one convincing conclusion brought out by these tests, it is the clear-cut superior performance of the stored-energy type of circuit breaker in handling the line-charging tests with no restrikes whatever. Although it may be premature to draw final conclusions, nevertheless, in the light of these and other tests, the evidence seems to be accumulating that the stored-energy type of circuit breaker will probably be the better answer to the high-voltage switching problem, at least in the extra high voltage field where insulation economies may be greatest.

On fault-interrupting tests, all of the circuit breakers tested showed remarkably dependable performance, extending to duties well above design ratings. It is especially gratifying to note that these circuit breakers, regardless of type or manufacture, showed consistent ability to interrupt the highest faults repeatedly, including intervals below 20 cycles. This confirms again the conviction we have had for a long time and which we have repeatedly expressed before the AIEE that derating factors on circuit breakers in the high voltage classes, at least 115

kv and above, are not necessary and should be eliminated. In the light of these tests, on top of many others showing similar results, we again submit that the time has come for realistic recognition of these facts and the eliminating by the manufacturer of such derating factors.

REFERENCES

1. See author's reference 9.

2. TRANSMISSION OF ELECTRIC POWER AT EXTRA HIGH VOLTAGES, Philip Spora, A. C. Monteith. AIEE TRANSACTIONS, volume 66, 1947, pages 1571–82.

F. J. Fry (University of Illinois, Urbana, Ill.): This paper presents a group of tests on competitive apparatus with no attempt made to compare the performance of the apparatus other than in the one very significant way that all tests on both manufacturer's circuit breakers were successful. There are a few very general remarks from which it is possible to make very rough comparisons but, of course, the positions of the authors would obviously not warrant any detailed comparisons.

However, since this paper does present data which can be analyzed in some detail and since competitive tests are virtually unknown, it should be interesting from several viewpoints to indicate how the various manufacturers have dealt with the problems encountered in these high power and line charging current interruptions.

Considering the line switching tests series "M" *Type FG-25*. Here the contact parting time is 1.9 cycles, and on the basis of a consideration of pure sine waves of current in a 3-phase system if the circuit breaker was capable of interrupting when current zeros occurred immediately after contact parting the minimum possible interrupting time would be 2.23 cycles which corresponds very well to the tabulated data. On the basis of the same considerations the maximum possible interrupting time would be 2.40 cycles which is 0.2 cycle less than that listed. This discrepancy could be explained on the basis of nonsinusoidal wave forms for the current and slightly longer contact parting times for some of the circuit breaker poles. There is also the possibility of a probability of clearing at a current zero which occurs at very short contact spacing which would mean that sometimes current zeros which occur for very short spacings are passed by with a negligible build up of voltage while at other times these zeros correspond to circuit interruptions. In any case it is apparent that the impulse circuit breaker in this form gives the ultimate in charging current performance. The peak voltage withstand ability at a time of 2.4 cycles which corresponds to the quickest time of maximum voltage application is at least 70-kv per inch.

No data in these particular field tests are available on the *Type GM-3*, or other circuit breakers, using the multi-flow deion interrupters from which any detailed information can be gathered, however, for those who are interested in the possibility of making more accurate comparisons, the master of science thesis "The Design of Low Mechanical Energy Charging Current Interrupters" submitted by F. J. Fry to the University of Pittsburgh will be of aid.

Specific data from this thesis in which the multi-flow deion type interrupter is used with a very small oil driving piston show that for contact speeds and oil velocities comparable to those used on the impulse circuit breaker the multi-flow deion principle can perform equally satisfactorily in the sense of giving the ultimate in charging current performance. There is of course very much doubt that such performance is really necessary especially since the multi-flow deion interrupter with its delayed action piston has proved to be very satisfactory.

For the high power tests, series "L" on the *Type FK-439* circuit breaker, the quickest interruption was in 2.1 cycles corresponding to a moving contact position which is somewhere between the two exhaust ports for the arc subject to the cross blast of oil. In the total pole unit there is approximately 4.5 inches separation in the interrupting arcs at this time and six cross blasts effective.

For the fault tests series "F" on the *Type GM-3*, the quickest interruption was in 1.8 cycles which corresponds to a contact position in the interrupting gap somewhere in the exhaust region past the first orifice. The total separation per pole in the interrupting gaps is approximately 2 inches and there are 2 orifices through which oil is flowing. To consider an interruption at 2.2 cycles, there are four orifices per pole effective and a total interrupting gap separation of approximately 3.5 inches.

These tests indicate that the particular orifice type of interrupter tested can function successfully at gap separations which are smaller than for the cross blast type. This is especially significant when it is realized that there are but two multi-flow deion interrupting breaks per pole whereas there are six interrupting breaks in the *Type KF-439* circuit breaker.

By considerations such as these, one can secure data which show how the various manufacturers have solved interruption problems, and by intelligent customer analysis of competitive apparatus on tests such as presented in this paper, it is to be hoped that judgment can be made in a sound engineering way on the merits of particular circuit breakers.

M. H. Hobbs (Westinghouse Electric Corporation, East Pittsburgh, Pa.): This and the companion papers record another important landmark in the development and testing of high voltage circuit breakers. We, as manufacturers, are very appreciative of the attitude of the Bureau of Reclamation and Bonneville Power Administration in providing the facilities and co-operation without which the tests could not have been made. They were somewhat of a continuation of similar tests made in December 1945, when fault currents up to 4,370,000-kva were interrupted, which at that time was also the maximum available anywhere. We are gratified that both series of tests confirm so closely the prior tests in our own high power laboratory.

The results show the performance of three different designs of circuit breakers, of two manufacturers, all of which appear to have the ability to interrupt high kilovolt-amperes, as well as charging current, at 230-kv.

With reference to the fault current tests on the *Type GM-3* circuit breaker, the performance shown by oscillogram, Figure 13, is quite remarkable, the 7,500,000-kva fault being interrupted in a total time of 1.85 cycles after the energizing of the trip coil, with a contact parting time of 1.1 cycles, and the fault current actually remaining on the circuit less than one full cycle. Although this was the shortest interrupting time, it was also on the highest fault current of any of the tests, and the average for all such shots on this circuit breaker was 2.5 cycles, with a maximum of 3.0 cycles this time occurring on only two tests of relatively low kilovolt-ampere from a single generator.

Similarly, the switching of line charging currents by the *Type GO-2* circuit breaker was excellent, as reported in the table of tests and shown by the oscillograms. No serious overvoltages were obtained, and, when equipped with the standard high ohmic value resistors, for which the circuit breaker was designed, the interrupting time was uniformly less than three cycles, with restrikes eliminated on all except a very few interruptions.

We agree with the authors that the tests again point to the desirability of using high ohmic value resistors, as explained in the Leeds, Van Sickle paper a year ago.[1] The low ohmic value resistors, whether used on the *Type GO-2* or the *FK-439* circuit breaker showed poorer performance and longer over-all interrupting time, although overvoltages were moderate, as expected.

In arriving at the best type and design of circuit breaker, all factors and characteristics must be considered together. Thus, charging current interruption is important, but high speed here is not as essential as on heavy fault currents, where the shock to the system must be relieved as early as possible to maintain stability. Obviously, charging current interruption must be such as to prevent overvoltages occurring which might cause insulation breakdown.

An interrupter, such as the multi-flow type, in which the oil flow is controlled by the arc itself, provides higher generated oil pressure across the arc path as the current increases, and consequently will give optimum performance on the higher kilovolt-ampere faults, as demonstrated in these tests. At the same time, in line with the test reports, the interrupter gives uniformly good performance on charging current interruption, using the relatively small and simple auxiliary piston to force oil across the arc path. Such a piston takes little power to operate.

Over the years the metal tank circuit breaker has shown its ability to provide in the simplest and most rugged form a device which has kept pace with the demands of the industry for higher voltages, high speed of interruption of both fault and line charging currents, improved system stability, and fast reclosing. At the same time, the physical construction of the circuit breaker accommodates current transformers and bushing taps for potential without the addition of external high voltage apparatus.

In addition, due to more efficient interrupters, the tank diameter for 230-kv has been reduced until it is within a few inches of that of the older 138-kv circuit breakers, condenser bushing designs have been further improved, and maintenance has been lowered.

With reference to the authors' conclusions,

it does not appear necessarily that the derating factor for 20-cycle reclosing can be eliminated for all designs of circuit breakers. On certain circuit breakers and voltages this may be entirely possible, and the 10,000,000-kva circuit breakers purchased by the Bureau for Grand Coulee are given their full kilovolt-ampere rating on the basis of 20-cycle reclosing. The circuit breaker manufacturers' committee is at present reviewing the entire derating factor standard and is expected to have a new table available in the near future, which will give greater recognition to the improved interrupting ability of modern circuit breakers.

It seems evident from the Grand Coulee tests with reclosing on arcing faults that the minimum time of approximately 12 cycles for such reclosures is a limitation of the system and not of the circuit breakers now in production, and that the demands even of this large concentration of power may be adequately taken care of by designs which are available.

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1. See author's reference 9.

S. B. Crary (General Electric Company, Schenectady, N. Y.): The field tests reported in the paper will remain an important milestone in the development of high voltage circuit breakers for the following reasons.

1. The possibilities of higher interrupting capacity at the higher voltage levels demonstrated by these tests allow for simplified bussing arrangements and greater circuit load carrying ability. These are important factors in realizing improved transmission economy for systems in general as well as for the case of Grand Coulee as described in the paper.

2. Elimination of high switching surge voltages as a factor in system design makes possible continued rational reductions in line and terminal insulation levels resulting in further system economies.

High voltage systems may now be confidently designed using the high voltage system as the backbone taking full advantage of modern American circuit breaker characteristics of

1. Quick clearing.
2. Quick reclosing.
3. High interrupting capacity.
4. Elimination of excessive switching surges.

These four advantages collectively will allow for greatly improved high voltage systems of high loading, reliability, and economy. Transmission system designers, therefore, are placed at the threshold of a new era largely due to the contributions of forward looking switchgear manufacturers and system planners.

J. P. Stratford (Department of Water and Power, Los Angeles, Cal.): There is little in this paper on which to base a discussion as all tests were successful, conclusions are not debatable, and no theories are developed.

There are, however, several interesting features brought out such as the fact that the most desirable bussing arrangement at Grand Coulee is the ring bus with all transformer neutrals solidly grounded. This would produce a circuit breaker duty approaching 15,000,000 kva which is out of the question at this time. By installing transformer neutral reactors and special bronze conductors between the two switchyards it is possible to reduce circuit breaker duty to approximately 10,000,000-kva.

Tests were made on four circuit breakers (three tank type, and one impulse type) at interrupting capacities approaching 200 per cent of their nameplate ratings (3,500,000-kva) as well as line charging and line dropping tests.

All tests were satisfactory although the line dropping tests using 1,250-ohm resistors required excessively long interrupting time.

An interesting feature of the tests was the use of pretripping to increase the duty on the test circuit breaker as well as to reduce the time the fault remained on the system.

The Department of Water and Power, city of Los Angeles, in considering the advisability of increasing the interrupting capacity at the 138-kv oil circuit breakers on their system, found that an increase in interrupting duty on the 138-kv oil circuit breakers resulted in increases in interrupting duty on the lower voltage circuit breakers throughout the entire system. It would be interesting to know if a similar situation will not arise in the Pacific northwest when the duty on the Grand Coulee circuit breakers becomes 10,000,000 kva. Will this result in the necessity of providing higher interrupting duty circuit breakers on all the apparatus, connected with the Grand Coulee power plant?

Lloyd F. Hunt (Southern California Edison Company, Los Angeles, Cal.): The problems of conducting field tests are very numerous and complex. It is necessary to record test data and arrange the system so the planned short circuit will be properly isolated without materially interfering with the normal supply of power.

The authors and all departments who contributed to these tests should be highly commended for their careful preparation and execution of these tests.

There has been very much improvement in oil circuit breakers in the past decade. The methods of testing and interpreting results of tests are still confusing. It is difficult to establish the values in the tables from the oscillograms. It is apparent that, although these were field tests, laboratory technique was used to obtain these high values of interrupting duty. Pretripping to get a high d-c component and decrement is according to Standard C37. It is known that with high d-c component the fault current is easier to interrupt because the voltage and current are in phase. Therefore the circuit breaker will have a great deal more duty to interrupt the same kilovolt-ampere without a d-c component.

A better way to determine a safe value of interrupting capacity of the circuit breakers is to have tests that simulate the most difficult types of conditions to interrupt the arc. When the contacts of an oil circuit breaker are just starting to open thus a very short arc length, very small amount of energy is dissipated, the arc voltage being about 100 per inch under oil. Therefore a peak occurring at this point is not a fair one to use for determining the duty of the circuit breaker. From a long series of tests for a given interrupter a very good average of arc extinguishing contact separation has been established for various duties. It seems reasonable to measure the current at this separation point or a half cycle preceding. If this value of current was taken on the heavy tests at Grand Coulee it would bring the duty nearer the rating of circuit breakers. This should be thoroughly checked before any of these switches are subjected to 10,000,000-kva short-circuit without d-c components. The Southern California Edison Company has made many series of 230-kv oil circuit breaker field tests where circuit breakers have shown considerable stress with the same kilovolt-ampere duty using regular relay tripping that were approved on pretripped laboratory tests.

W. G. Hoover (Stanford University, Stanford, Cal.): The Bureau of Reclamation and co-operating organizations are to be commended on their interest in and willingness to make such important tests as described in this paper. The demonstration of the small shock to the station equipment and connected systems produced by pretripping the test circuit breaker should help make available more direct tests at other locations of large power concentration throughout the country.

The presentation of cathode-ray oscillograms of voltages produced across the test circuit breaker in both fault and line-dropping tests would have been of interest.

Both from the standpoint of the modern as well as the somewhat spectacular, this paper could well be called to the attention of students of electrical engineering as evidence that the "romance" of electric power engineering is far from a thing of the past.

O. A. Demuth (Bonneville Power Administration, Portland, Ore.): For several years, calculating board studies including proposed additions to the Bonneville Power Administration system, with very large concentration of generation at Grand Coulee and other projected dams, have clearly indicated that there was an urgent need for high-voltage oil circuit breakers having ratings greater than the 3,500,000-kva which manufacturers were offering, and that steps resulting in the development of circuit breakers having higher interrupting capacities should be taken. We believe that the Bureau of Reclamation engineers are to be commended for their foresight in this regard and for their willingness to use the Grand Coulee power plant generating equipment for such tests.

We also wish to compliment the authors of this paper on fully and adequately describing the conditions of the field tests, the results of which are presented in the two companion papers prepared by representatives of the manufacturers who participated in the tests. We are sure that these particular tests have resulted in materially contributing to the art.

The Bonneville Power Administration has frequently made staged tests on their system particularly at times when making major additions to the system. Such tests ordinarily include application of system faults and the interruption of line-charging current in order to check the operation of circuit breakers, co-ordinate protective equipment, and eliminate possible weak spots in equipment being added before it goes into regular operation. We are convinced that such tests have been well worth while, because

the analyses of oscillographic and other records obtained, and observations made during the tests, have not only given us as well as the equipment manufacturer valuable data for development of more reliable equipment and larger capacity high-voltage circuit breakers, but have also brought to light many weak spots—such as inadequate methods of relaying, improperly connected control and metering circuits, and improperly adjusted equipment and devices. If such conditions had not been found and corrected at that time, they might have later resulted in costly system interruptions, the cause of which then would have been difficult to isolate because such interruptions would have occurred at a time when no specially planned back-up protection, methods of observation, or metering were in effect.

The previous tests conducted by the Bonneville Power Administration were at points on the system remote from large generating stations such as the Coulee power house and, consequently, the faults to which the circuit breakers were subjected did not result in fault currents of the large magnitude obtained in the tests described by Killgore and Clagett.

We believe that field tests such as reported in the paper are mutually beneficial to the manufacturers of switching equipment and to the power utilities on whose system the equipment is to be used. System testing, when conducted under properly controlled conditions with suitable backup protection and adequate measuring facilities, in many cases is the only method by which the manufacturer and the utility engineer can be sure that newly developed equipment will be suitable for the operating conditions to which it will be subjected when used on large power systems.

T. M. Blakeslee (Department of Water and Power, Los Angeles, Cal.): With regard to line-switching tests on the *Type GO-2* circuit breaker, it is stated that the switch was 93 per cent open on one test before the charging current was interrupted. Although the dead-time characteristics of this resistor-equipped circuit breaker are not given, the difference between the maximum and minimum interrupting times presented in Table I indicates that arcing on the contacts interrupting the resistor current was in the order of five-cycles duration. Apparently the *Type GO-2* circuit breaker does not lend itself to the application of resistors which would guarantee a limit to overvoltages that may result from restriking when interrupting charging currents.

The tests with the resistor-equipped *Type FK-439* circuit breaker show that the resistor is in the circuit an appreciably shorter time than with the *Type GO-2* circuit breaker. The performance data given in the paper indicate a satisfactory resistor installation. Evidently there was no concern about the *Type FK-439* circuit breaker as no line-switching tests on this switch without the advantage of the resistor were made.

It would be helpful if the authors would give the dead-time characteristics of the two types of resistor-equipped oil circuit breakers in their closing discussion.

The results of the line-switching tests with the *Type FG-25* impulse circuit breaker confirm the experience of the Department of Water and Power. Tests with the impulse circuit breaker interrupting charging currents of 90 and 180 miles of 287.5-kv transmission line were made on the department's system in 1939 and 1943. Performance of this type of circuit breaker handling 80 amperes of charging current to 5 miles of 138-kv underground cable was demonstrated in 1944. In every case clean-cut interruption of the charging current occurred without restrikes and with no transient disturbance to system or circuit voltages.

The high-current interruptions reported by Killgore and Clagett were obtained by pretripping the test circuit breaker. This effected interruption at high rms values of current because of the heavy d-c component although the interruption was easier for the circuit breaker because of the reduced value of instantaneous voltage at current zero. While the interrupting claims of the paper are justified on the basis of present American Association Standard *C37.4-1945 Alternating-Current Power Circuit Breakers*, perhaps it is time that this standard be reviewed. Visitors from abroad are confused by this American practice which, they say, builds up circuit-breaker ratings unjustifiably. The growing international activities of the AIEE and the active interest in the International Conference on Large Electric High Tension Systems, might make it advantageous to review the American Standard in order to put it on a universal and more correct basis for rating the interrupting capacity of circuit breakers.

The authors and others associated with these tests are to be commended for the care and attention given to details of protection and measurement in this epoch-making work. They have made a valuable contribution which has led to an important step in the evolution of the circuit breaker.

C. L. Killgore and **W. H. Clagett:** The authors are very gratified at stimulating so much interest on the subject of high-capacity field testing of 230-kv circuit breakers, as evidenced by the large number of discussions which were received, and wish to thank S. B. Crary, W. G. Hoover, M. H. Hobbs, F. J. Fry, O. A. Demuth, Philip Sporn, J. P. Stratford, Lloyd F. Hunt, T. M. Blakeslee, and H. P. St. Clair for their constructive and pertinent discussions of the paper.

Both Hunt and Blakeslee point out the fact that even though circuit breaker testing with high values of d-c component of short-circuit current by pre-tripping is in accordance with present standards (*C37.4-1945, Alternating Current Power Circuit Breakers*), the interruption is actually easier on the equipment than if the same equivalent kilovolt-ampere were obtained without the d-c component, because the voltage and current are in phase. Since interruption of a short-circuit with a high d-c component occurs at reduced values of instantaneous voltage, it is easier from this standpoint for the circuit breaker to extinguish the arc. However, it was our observation during the circuit breaker tests that the mechanical shock to the circuit breaker was always noticeably greater on interruption of a short circuit with a high d-c component than on interruption of an equivalent symmetrical fault.

Pretripping during circuit breaker testing both in the laboratory and in the field is accepted practice. During laboratory testing, pretripping is generally used for the sole purpose of obtaining proportionately higher fault current by addition of d-c component at the instant of contact parting, thus resulting in higher interrupting duties than could be obtained otherwise. In the case of field tests, pretripping is likewise used for this purpose, but, of equal importance, it is also used to (a) provide a positive method of tripping the test circuit breaker by means of a simple "a" auxiliary switch, independent of relays and current transformers, with the latter equipment acting as backup protection, and (b) to provide high-speed removal of the fault from the power system to reduce system disturbances to a minimum. In view of the foregoing three functions, pretripping during the Coulee tests, particularly on the higher capacity shots, was adopted as the safest policy from an over-all system standpoint.

It is admitted that because of the high values of d-c component obtained on some of our near maximum tests, the computed kilovolt-ampere may be considered to be slightly inflated in respect to actual circuit breaker duty, or in respect to a similar test of equal kilovolt-ampere without d-c component, but during the Coulee tests this situation was more than compensated for by the fact that the tests were repeated a large number of times without the benefit of contact inspection. In fact, the test series as completed could have been considered more of a life test than an interrupting-capacity test, for in some instances as many as twenty faults were applied in one series; the purpose being to obtain data on contact deterioration and oil carbonization.

Hunt expresses apprehension over the use of new circuit breakers rated at 10,000,000 kva on the ultimate Coulee ring-bus without first testing them at this rating, without d-c component. As was pointed out in our paper, the concentration of eighteen 108,000-kw low-reactance generators at Grand Coulee power plant causes high values of d-c component for offset fault currents. The decrement factor for a fully offset fault at the 230-kv switchyard bus with all generators and transmission lines in service is approximately 1.35. On this basis the symmetrical component of a 10,000,000-kva short circuit for this particular application will not exceed 7,500,000-kva. Consequently, circuit breakers tested at 10,000,000-kva with some d-c component should be adequate for the proposed installation, particularly if they are given a number of repeated shots beyond the single standard duty-cycle required by the ASA standards.

Both Hunt and Blakeslee have suggested that the present ASA method of determining the interrupting capacity of circuit breakers should be reviewed. Blakeslee has suggested that curcuit breakers should be rated on a symmetrical current basis which is in accordance with European practice, and believes that the increasing exchange of technical knowledge through the International Conference on Large Electric High Tension Systems presents an excellent opportunity for discussion of this question. We agree with Blakeslee and have recommended to the EEI–AEIC–NEMA* Joint Com-

* Edison Electric Institute—Association of Edison Illuminating Companies—National Electrical Manufacturers Association.

mittee on Power Circuit Breakers that consideration should be given to rating circuit breakers on a symmetrical current basis. This procedure will greatly simplify the application of circuit breakers to power systems by eliminating consideration of the d-c component of fault current which depends for the most part on the subtransient reactance of the generators and the distance the circuit breakers are from the generators. Low reactance generators are not required for European power systems because of the relatively short transmission distances, which may account to a certain extent for the European method of rating circuit breakers. If a symmetrical current basis of rating is adopted it would be necessary for circuit breaker manufacturers to adequately design for any additional stresses imposed on the circuit breakers by the maximum possible d-c component of fault current. Hunt has suggested that the present method of rating circuit breakers at the instant of contact separation is questionable and believes that duty current should be measured at the average point of contact separation when the arc is extinguished, or 1/2 cycle earlier. This would in effect tend to rate circuit breakers on a symmetrical basis since much of the d-c component of current will decay in the additional contact parting time.

Blakeslee presents a comparison between the rather excessively long arcing time of five cycles for the *Type GO-2* circuit breaker, as compared with a considerably shorter time for the *FK-439* circuit breaker, when interrupting line-charging current. Both circuit breakers were equipped with low-ohmic value resistors during these tests. As pointed out in our paper, the *Type GO-2* circuit breaker was not designed for this type of resistor; the resistor having been applied to an early model for experimental purposes only. One of the basic factors contributing to the apparent difference in arcing time is due to the fact that the movable crosshead in the *Type GO-2* circuit breaker is engaged in finger contacts, whereas the *FK-439* circuit breaker utilizes butt contacts. This is not a valid comparison as the *GO-2* circuit breaker was designed for and is normally equipped with the so-called high-ohmic value resistors resulting in satisfactory performance. We concur with Blakeslee's comments regarding the experience which the Department of Water and Power, City of Los Angeles, has had, as well as similar comments by Sporn and St. Clair in regard to the clean interruption of line-charging currents by the stored-energy type of circuit breakers, without evidence of restrikes or transient voltages. While our papers stressed the fault tests at Coulee, the tests on interruption of line-charging current and the successful results obtained were of equal importance. As pointed out by Sporn and St. Clair, elimination of switching surge voltages permits lower insulation levels on high-voltage equipment.

Stratford has raised the question of whether the increase in circuit breaker duty at Grand Coulee to 10,000,000-kva will likewise result in a proportionate increase in circuit breaker duty throughout the Pacific northwest. The answer is yes. However, the increase in circuit breaker duty is due to the continued installation of new generating units at Grand Coulee, since only eight of the ultimate eighteen units are installed at present. Calculating board studies of a large number of schemes for bussing the eighteen generating units, of 108,000-kva each, has indicated that it makes very little difference to the rest of the system, in respect to circuit breaker duty, how Grand Coulee is bussed. However, increased short-circuit current only reaches out to the first stations on the 230-kv transmission system, approximately 100 miles in each of three directions. The effect upon circuit breaker duty beyond the first station is negligible.

We appreciate the comments of Demuth, Sporn, St. Clair, Hunt and Hoover that it was gratifying to see a high-capacity interconnected power system used for field testing, and it is our hope that the successful performance and lack of disturbances on the system during these tests will encourage others to use their power systems for field tests. Such tests are of mutual benefit to both the manufacturers and power system designers and operators, and we believe they are essential to the continued development of the electrical industry. We would like to repeat Sporn's and St. Clair's comment that the hazards of staged fault tests such as those at Coulee are much less than the normal hazards of lightning, and the shock to the generators and other equipment is also much less than that for which they were designed. We agree that it would have been well to point out that if the tests disclosed equipment weaknesses, it would be better to find this out during carefully observed field tests than during normal operation.

Hobbs points out that while the Coulee tests indicate no de-rating factor is required for one 20-cycle reclosure on the circuit breakers tested at Coulee, this does not necessarily mean it can be eliminated for all circuit breakers. Our comments in respect to this matter apply to high-voltage circuit breakers of the 115-kv class and above, and we are convinced on the basis of the tests at Grand Coulee that no de-rating factor should be applied to high-voltage circuit breakers. We agree with Hobbs' observation that the speed with which an arcing fault can be reclosed without restriking is a function of the power system and not a limitation of the circuit breaker. Our tests indicate that modern reclosing circuit breaker designs will permit reclosing in 12 cycles or less.

Crary's comment that the field tests at Grand Coulee power plant are an important milestone in the development of high-voltage circuit breakers is appreciated. We consider the development of ultra-high interrupting capacity circuit breakers as one of utmost significance to power system designers and planning engineers since higher interrupting capacity circuit breakers permit simplified high-voltage bussing arrangements and greater load carrying capacities. The development of 3-cycle clearing, 20-cycle reclosing and 10,000,000-kva interrupting capacity circuit breakers which eliminate overvoltages from switching surges was only made possible by continued engineering development and foresight by the manufacturers and confirmation of new circuit breaker designs by field testing.

The authors wish again to acknowledge the excellent co-operation and assistance of all those who participated in these tests and especially that of L. N. McClellan, assistant chief engineer of the United States Bureau of Reclamation, whose vision and courage made these tests possible.

A Faster Telemeter for Carrier-Current Channels

E. E. LYNCH
MEMBER AIEE

H. C. THOMAS
MEMBER AIEE

G. S. LUNGE
ASSOCIATE AIEE

Synopsis: This new frequency-type telemetering system combines continuous measurement and fast response with high accuracy and stability of calibration. It uses as a translating means an alternating electric signal whose frequency is directly proportional to the measured quantity. This signal originating in a contactless transmitter can be used to modulate or key carrier-current channels of conventional fixed frequency types. It is the frequency of this signal rather than its magnitude which is detected by the receiver and converted without moving parts into a proportional direct current sufficient to operate a conventional indicating or recording instrument. This system in its basic form was developed to telemeter a-c watts, vars, volts, and amperes. Auxiliary equipment is available to permit telemetering practically any other electrical or nonelectrical quantity.

ELECTRICAL telemetering involves conversion of the quantity being telemetered into some proportional electrical quantity for the purpose of transmission to the distant receiving instrument. This implies a separation of the primary detector and the end device.[1] For many purposes, especially when used in automatic control schemes,[2] such as tie-line load control, it is essential that this conversion into the proportional intermediate quantity for transmission, and the subsequent conversion of that quantity into the final reading or control signal, be accomplished with minimum time lag. For a particular control purpose where it is desirable to introduce a time lag to make the automatic controller unresponsive to minor fluctuations in the controlled quantity, the lag is preferably provided, in adjustable amounts, in the controller itself.

For many telemeter applications involving automatic control a telemetering system, involving discontinuous action, cannot be tolerated, as it would seriously handicap the smooth, continuous, and timely action of the controller. An ideal telemetering system must, for best flexibility, not only have a rapid response, but it also must provide a continuous output signal.

Where a carrier-current channel is used, it is essential that the telemetering system require as narrow a carrier-frequency band width as possible. A high degree of accuracy, stability of calibration, ease of calibration and maintenance, and long life are additional characteristics to be sought in the ideal telemetering system.

The torque balance telemeter[3] meets all of these requirements, but is only applicable to well-shielded full metallic pilot wire channels.

Wherever distances are at all appreciable, carrier-current channels are usually necessary or at least preferred for telemetering.[4] Where power transmission lines are available, such carrier-current channels will usually employ frequencies somewhere in the 30 to 200 kc band, the particular frequencies being carefully chosen with a view to avoiding interference with other carrier facilities on the same or neighboring power systems.

Hitherto most telemeters for use with carrier-current channels have employed at the receiver an essentially square wave impulse signal, the rate of which, or the duration of which, could be varied in proportion to the magnitude of the quantity to be telemetered. With such impulse transmission, the telemeter receiver action has been very slow in following a rapid change in the measured quantity, actually discontinuous, or both. Because transmission of impulses by a carrier-current channel involves square wave keying, (essentially modulation[5] by a rectangular wave of signal frequency), simply increasing the frequency of impulses transmitted is not ordinarily an acceptable method of increasing the speed of response. This is because the required carrier band width, which is relatively large due to the square wave keying, is further increased by the faster keying rate, with the consequent reduction in the number of simultaneous readings that can be transmitted by the same carrier-current channel.

The New Fast Responding Frequency-Type Telemeter

To overcome these inherent difficulties in impulse telemeters and to eliminate sources of mechanical maintenance problems, yet without requiring unusual or undesirable types of carrier-current channels, the new frequency-type telemeter as shown in Figure 1 was developed. In this system the frequency of the transmitter signal varies in proportion to the magnitude of the measured quantity, over the range from 6 to 27 cycles per second. Frequency was chosen as the quantity to be recognized by this system because it is entirely independent of the transmission channel characteristics which are variable in nature. This range in frequency was selected to provide the most desirable balance between fast time of response, minimum carrier band width requirements, and minimum instrument damping. Higher frequencies would require excessive band widths; lower frequencies would require excessive damping, destroying quick response. The use of a sinusoidal transmitter signal, instead of the more common square wave impulse signal, permits better utilization of the carrier-current equipment of the type employing audio tone modulation of the

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E. E. Lynch is development engineer and H. C. Thomas is engineer with the General Electric Company at West Lynn, Mass. G. S. Lunge is with the central station engineering division, General Electric Company, Schenectady, N. Y.

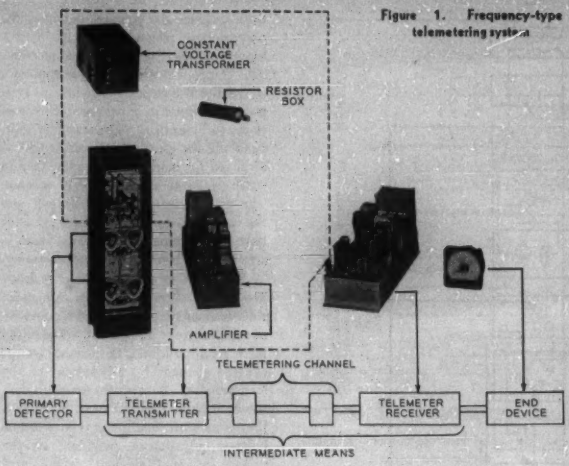

Figure 1. Frequency-type telemetering system

carrier. The frequency-type telemeter is a logical outgrowth of the magnetic impulse telemeter,[6] an improved form of which, as long ago as 1936, was provided with a photoelectric impulse-originating device and a voltage squared constant torque driving element in the transmitter. The 1936 receiver, however, was a thermal storage microammeter and was consequently somewhat sluggish, besides not being suitable for recording or control initiating purposes.

The Transmitter

Figure 2 illustrates functionally a telewattmeter transmitter.

The primary detector is a watthour meter of standard construction. It is connected to the circuit to be measured in the usual manner, and produces a shaft rotation at a speed which is proportional to the input watts. Similar detectors can be used to measure vars, a-c volts, or a-c amperes.

A voltage whose frequency varies in proportion to the measured quantity is generated by means of a photoelectric pick-up actuated by the rotation of the watt-hour meter shaft. A disk having a serrated edge is fastened to the watthour meter shaft. Located above the serrated disk is a light source, and below it a photoelectric tube, arranged so that rotation of the disk modulates the light beam directed on the tube. The shape of the serrations is such that a sinusoidal output voltage is obtained from the photo tube. The number of serrations is such that the frequency of the output voltage, with maximum rated input, is approximately 27 cycles per second.

The output of the photo tube is fed directly to a cathode follower, which serves primarily to reduce the output impedance of the pick-up mechanism. The cathode follower is physically located close to the photo tube, and, by its use, interference from external electrostatic and electromagnetic fields is made negligible.

The output voltage from the cathode follower is fed to a two stage push-pull amplifier of conventional design. The amplifier output is transformer coupled, thus providing complete electrical isolation. The amplifier also may include and operate an electromechanical relay when required for keying. The voltage and power output of this amplifier are of sufficient magnitude to operate most carrier-current equipments directly.

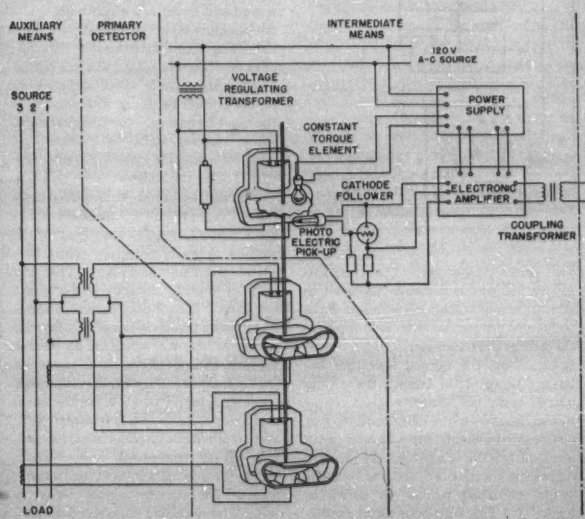

Figure 2. Frequency-type telemeter transmitter

Schematic functional diagram

An additional torque-producing mechanism is arranged to act on the watt-hour meter shaft, for the purpose of providing a finite shaft rotation when the measured quantity has zero magnitude. By this means, a definite distinction is made between zero input and inoperative transmitter, thus providing at the receiving station an indication of transmission outage. This means also is used to provide zero center operation. This additional torque is produced by a voltage squared type watt-hour meter element which utilizes the serrated disk as its driven member. A resistor is connected in

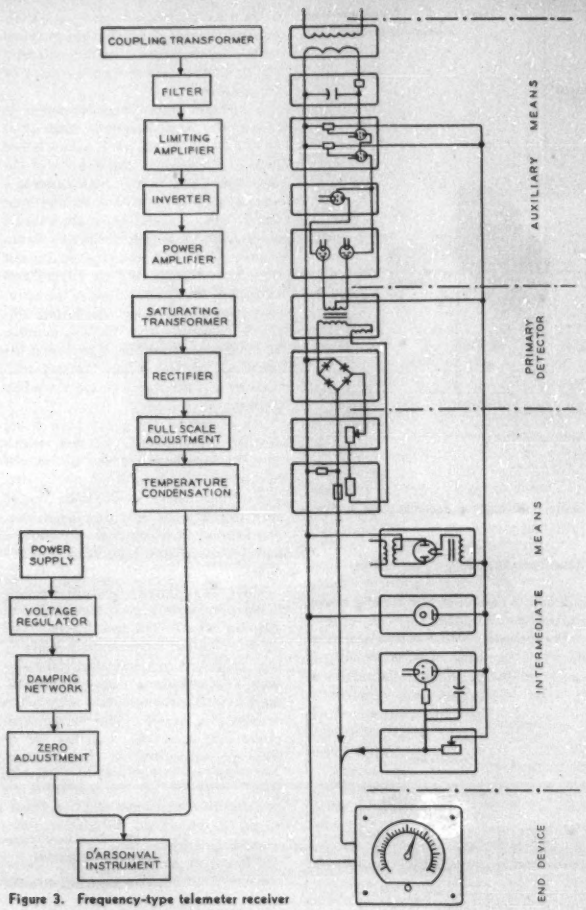

Figure 3. Frequency-type telemeter receiver

Schematic functional diagram

series with one of the element windings to provide the necessary calibration. This resistor has essentially a zero temperature coefficient, and is made very large compared to the resistance of the element winding in order to minimize ambient temperature errors. The voltage-squared element is excited from the station supply through a voltage regulating transformer, which provides a constant output voltage over normal variations in input voltage and frequency. Thus the torque produced by this element is constant, and adds directly to the torque produced by the primary detector. The constant-torque element is normally adjusted to give an output voltage whose frequency is six cycles per second with zero input to the primary detector (for zero-left operation). Thus the normal operating frequency range of the transmitter is from 6 to 27 cycles per second.

The transmitter is comprised of four physical components, the watt-hour meter mechanism, the amplifier, the voltage regulating transformer, and the constant torque adjusting resistor as shown in Figure 1. The watt-hour meter mechanism includes the primary detector, the photoelectric pick up, the cathode follower, and the constant torque element. This component very closely resembles a conventional 3-element switchboard type watt-hour meter in appearance, construction and mounting. The amplifier unit, including the electronic power supply, is arranged for standard rack mounting in either vertical or horizontal position. The voltage regulating transformer and the constant torque adjusting resistor have their own mounting means; each is arranged for behind-the-panel mounting.

Calibration of the transmitter primary detector is accomplished by the usual watt-hour meter practice, and requires no new technique or equipment. The initial factory calibration of the constant torque element is made by adjustment of the load resistor. Subsequent recalibration, should it be necessary, can be made with the normal watt-hour meter type of torque balance adjustment included in the voltage squared element, which provides ample range for normal recalibration. This calibration may be made using the same stop-watch technique employed to calibrate watt-hour meters.

The Receiver

The receiver is a frequency meter with a linear scale and with a high degree of immunity to response from interfering signals. Its purpose is to receive a frequency signal of from 6 to 27 cycles per second and to furnish a continuous, fast responding output to operate standard indicating, recording or control initiating devices. Since totalizing with the output of other receivers is often required, its response must be linear with respect to the input frequency. Most important of all, its response must be consistent, it must have long life stability, comparable to that of the transmitter.

The receiver is shown in simplified functional form in Figure 3. Auxiliary means are required to prepare the signal for the receiver's primary detector. These consist of a coupling transformer, a filter network, a limiting amplifier and a power amplifier. The primary detector itself is a saturating transformer and rectifier, the secondary output of which has an average value dependent only on the frequency of the input signal. Intermediate means are required to supply power to the electronic components, to furnish a regulated voltage, to provide a biasing current, to provide ambient temperature compensation, and to provide full and light load adjustments. The receiver output signal is suitable for operating various end devices,

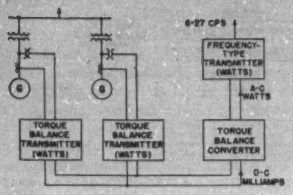

Figure 4a. Telemetering totalized quantities

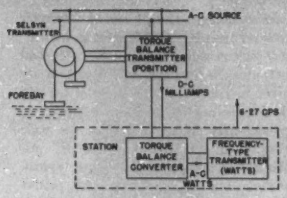

Figure 4c. Telemetering position

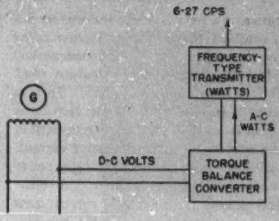

Figure 4e. Telemetering d-c quantities

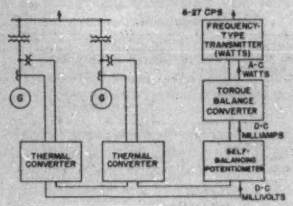

Figure 4b. Telemetering totalized quantities

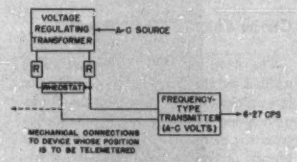

Figure 4d. Telemetering position

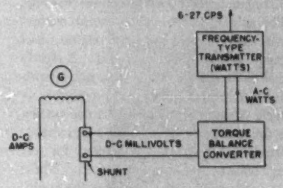

Figure 4f. Telemetering d-c quantities

Figure 4. Frequency-type telemeter transmitter applications

such as a D'Arsonval indicator, a recorder, a control initiator, or means for totalizing.

Auxiliary Means

The various auxiliary means shown in Figure 3 constitute an extremely important portion of the receiver. In preparing the input signal for the primary detector, the coupling transformer insulates the receiver circuits from the carrier channel.

The filter eliminates the audio tone frequencies and assists the limiter in accomplishing the important function of eliminating unwanted signals. The amplifier is provided to insure complete saturation of the transformer.

One of the most severe situations where the elimination of unwanted signals is necessary arises when the receiver is one of several used with carrier-current equipment designed to transmit a number of signals over a single carrier-current channel. This is usually done by modulating audio tones in the range from 460 to 3,070 cycles per second with the output signal of the telemeter transmitter, mixing the audio tones and modulating the carrier frequency with the combined signal. At the receiver, the carrier is demodulated; the audio tones separated by filters, and each audio tone demodulated to obtain the telemetered signal.

In this case, two major types of interference are present. Both are due to the fact that the separation of the audio tones in the carrier-current equipment is not perfect. The first interference possibility is encountered when the desired signal is zero. The interfering signal is then the only input to the telemeter receiver, but its voltage magnitude is normally low. To provide for this situation the receiver amplifier is biased so that any signal below six volts is indicated as no signal.

Another interference situation occurs when both a desired and an undesired signal are present in the telemeter receiver input. A simple method of eliminating the effect of this interference signal is to have the telemeter receiver count the number of times the signal crosses the zero axis. This makes it practically independent of wave shape. It is accomplished in the limiting amplifier shown schematically in Figure 3 by delivering an essentially rectangular wave to the amplifier independent of the wave shape of the input signal.

However, the bias is selected so that any original signal less than six volts will deliver a signal to the second tube entirely below cut-off and will result in no plate signal to the amplifier. This novel method of eliminating both types of interference signals in a frequency meter has proved satisfactory and makes the receiver extremely flexible as far as use with various types of carrier-current equipment is concerned.

Primary Detector

The rectangular wave output of the amplifier is impressed on the saturating transformer, shown in Figure 3, and the core is saturated by flux in opposite directions each half cycle. The secondary current per half cycle is therefore

$$2F\int_{-\phi_m}^{+\phi_m}\frac{N}{R}d\phi=\frac{4Nf\phi m}{R}$$

where

N = number of turns on saturating transformer secondary
ϕ = transformer flux and ϕ_m = maximum flux
f = frequency
R = resistance of secondary circuit

Special precautions are taken in the choice of material and the shape of the core iron to insure that ϕ_m is a constant for any exciting voltages of the range to be encountered. The current per half cycle is therfore proportional to the frequency only, since the resistance R can be kept constant. This proportionality exists up to the maximum frequencies encountered in use. The stability of the primary detector is thus dependent only on certain very stable magnetic proporties of the core iron.

If during test, excessive frequencies are impressed on the transformer, the time constant is such that at the higher frequencies the flux cannot build up to values that will result in serious overcurrent

Intermediate Means

The output from the saturating transformer, which is a wave of positive and negative pulses, is rectified as shown schematically in Figure 3. The average d-c component is suitable to operate a D'Arsonval instrument to obtain an indi-

cation or a record. The transmitter has a constant biasing torque corresponding to six cycles (for a left instrument) which results in a receiver output when the transmitter output is zero. To take care of instances where the zero-input signal is not desired, there is generated in the receiver a constant negative current, furnished from a standard type power supply and voltage regulator. A resistance to adjust this constant current acts as a zero adjustment for the receiver. With this arrangement it is possible to distinguish three types of zero reading. The sub-zero from the constant current indicates receiver operation but no input signal, not even the six cycles. A steady zero indicates the receiver is not operating. A zero reading with a slight six cycle vibration indicates that the system is functioning with six cycles (zero magnitude of quantity telemetered for left zero scale ranges) being indicated. These features are an aid in checking the equipment after installation, or in case of outage of the carrier channel. In addition, sub-zero readings can be used for special signaling purposes.

Conservative design in the use of electronic tubes insures relatively long life; and outputs large enough to allow proper swamping resistors, as shown in Figure 3 to obtain good temperature compensation and to swamp out the effects of rectifier changes are characteristic of the high accuracy and good stability of the receiver.

Adjustments at both the zero point and an up-scale point are available and are indicated in Figure 3. Changing the zero point will alter all points on the scale by the same amount. Changing the up scale point will alter the zero by about one quarter the amount of the up-scale point change. Both adjustments are simply made and can be done independently of the transmitter.

End Device

An indicating end device is shown in Figure 3; however an indicator, recorder, or both can be used if the proper equivalent load is maintained. These end devices have a full scale rating of one milliampere. Where extreme accuracy is required, accurate end devices may be used. Where less accuracy is needed, lower cost instruments are suitable and useable in the system.

Performance Characteristics

A typical telemetering system might consist of a telewattmeter transmitter, a carrier-current channel and a telemeter receiver arranged for operating an indicating or recording instrument. Typical performance data for such a system are given in Table 1. The effects of the end device are not included, since this component may be selected from the wide variety of available instruments. Although the accuracies shown are typical, adjustments are furnished which permit the attainment of greater accuracies if under certain conditions it is desirable.

Carrier Channel Requirements

The 6 to 27 cycle frequency range utilized by the frequency-type telemeter is entirely suitable for either amplitude-modulation of a fixed audio-frequency tone that modulates the carrier frequency proper, or keying, electronically, a frequency-shift carrier telemeter channel. It is somewhat low for convenient direct amplitude-modulation of carrier frequencies in the 50 to 200 kc band. When keying a frequency shift channel it is convenient to have one carrier-current frequency transmitted during positive half cycles of the 6 to 27 cycle telemeter frequency, this carrier-current frequency being replaced by a slightly different frequency during the negative half cycles. Since the frequency shift is small, a large number of quantities can be telemetered simultaneously over the same route. Another method of telemetering a number of quantities over the same wires is to modulate audio-frequency tones that modulate the carrier. By the use of several simultaneous tones, a number of telemeter readings can be transmitted by the same carrier frequency. Of course, wherever more readings are involved a number of carrier frequencies can be used in the 50 to 200 kc frequency band.

The output of the amplifier associated with the telemeter transmitter is approximately 30 volts, at all frequencies, when feeding into an impedance of 50,000 ohms. The telemeter receiver requires an input of approximately 15 to 30 volts, across an impedance of 10,000 ohms, from the associated carrier-current receiver. Both the output characteristics of the transmitter and the input characteristics of the receiver are therefore suitable for use with commercial carrier-current equipment.

Table 1. Typical Performance Data

	Per Cent of Full Scale
Accuracy	1.0
Precision (repeatability)	0.5
Influences:	
Voltage (±10% of rated)	1.0
Temperature (±25 C from 25 C)	1.0
Frequency (±0.5% from rated)	1.0
Speed of response	Instantaneous (Less than 1 second)

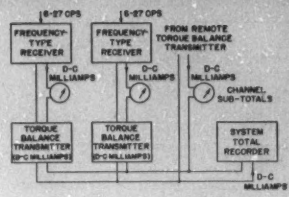

Figure 5a. Remote totalization

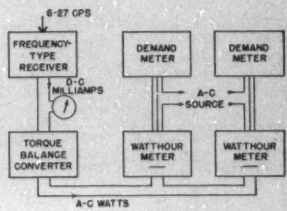

Figure 5b. Telemetering watt-hours and demand

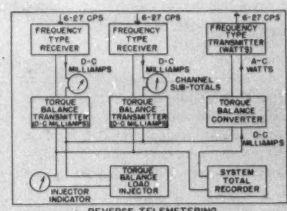

Figure 5c. Reverse telemetering

Figure 5. Frequency-type telemeter receives applications

Transmitter Applications

Figures 4 and 5 cover various ways of utilizing the frequency-type telemeter and show a number of combinations with other telemetering equipment. For carrier telemetering of quantities other than watts, vars, a-c amperes or volts, and for totalizing, auxiliary devices are used.

For the very common requirement of totalizing watts or vars at the transmitting end of the channel, torque balance telewattmeter transmitters may be used to advantage, Figure 4a. The paralleled d-c milliampere outputs of these transmitters are converted, with negligible time

lag, into proportional single phase watts by a torque balance converter.[7] The converter single phase output is suitable for directly operating a frequency-type telemeter transmitter. This method of totalizing is used when lack of a common source of potential or other considerations preclude paralleling current transformer secondary windings through auxiliary current transformers to operate a single telewattmeter transmitter.

The torque balance converter is also applicable for telemetering d-c milliamperes. The milliamperes might be obtained from a torque-balance telemeter channel from a nearby location, such as for forebay water level telemetering. Such an application is illustrated in Figure 4*c*.

For telemetering of mechanical position, such as transformer tap position, the position is converted into a proportional alternating voltage for telemetering, Figure 4*d*. This can conveniently be done by a small rheostat or tapped resistor across which regulated a-c voltage is maintained.

When a torque-balance converter is used with a frequency-type telewattmeter transmitter for telemetering d-c volts, such as the field volts of a synchronous generator or condenser, or d-c millivolts from a shunt in a d-c circuit, the torque balance converter is built with a d-c voltmeter, Figure 4*e*, or millivoltmeter Figure 4*f* basic element, instead of the usual d-c milliammeter element. When the source of direct voltage to be measured must not be called upon to supply any current, for example, in the case of thermal converters, thermocouples, or retransmitting slide wires in telemeter recorders, an electronic high speed self-balancing potentiometer is used between the direct voltage to be measured and the torque-balance converter. This potentiometer provides a d-c milliampere output proportional to the millivolts, or even microvolts, input to it, Figure 4*b*.

Receiver Applications

When it is necessary to totalize several frequency-type telemeter readings, and simultaneously provide individual readings and subtotals, or when it is necessary to employ individual telemeter receiver output for automatic control purposes, it is advisable to use the torque-balance telemeter retransmitter. By this means the desired milliampere range is obtained with negligible additional time lag, Figure 5*a*. The use of such a retransmitter also makes it possible and convenient to totalize torque balance telemeter readings together with frequency-type telemeter readings. When totalizing with other types of telemeters providing only d-c millivoltages from their receivers, a self-balancing potentiometer will convert such millivoltages to proportional d-c milliamperes that can be totalized with the d-c output of the frequency-type telemeter receivers.

If integration of frequency-type telemeter readings, either individual or totalized, is desired at the receiving point to provide kilowatthour or kilowatt demand readings, this is readily accomplished by means of a torque-balance converter, the single phase a-c output of which can operate one or two watt-hourmeters, one for each direction of flow, in the case of reversible flow. The watthour meters can have kilowatthour registers and also, if desired, contact devices to operate any conventional type of block interval demand meters, for one or both directions of energy flow, Figure 5*b*.

For telemetering a system total or subtotal load, as totalized at the dispatcher's office, to individual generating stations for their guidance the system shown in Figure 5*c* is applicable. In this case, outage of one or even all telemeter channels need not mean dispensing with the totalized reading, as a manually-set torque-balance load injector[7] can be used to replace the missing telemeter reading or readings with approximate exactness.

When large diameter indicating receivers are desired, these may be selsyn operated from transmitting selsyns mounted in the strip chart recording milliammeters, via indication transmitters as torque amplifiers.[8]

A system incorporating most of the foregoing applications has been in service for some time to obtain operating data. The equipment is illustrated in Figure 6.

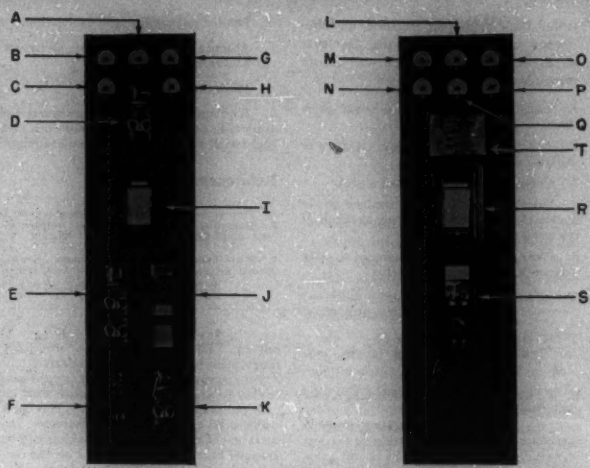

Figure 6. Telemetering systems test panels

A—Station number 1 volts
B—Station number 1 generator number 1 megawatts
C—Station number 2 megawatts
D—Station number 1 voltage transmitter, frequency type
E—Station number 2 watt transmitter, frequency type
F—Station number 1 converter, for totalizing
G—Station number 1 generator number 2 megawatts
H—Station number 3 megawatts
I—Station number 2 megawatts
J—Station number 3 watt transmitter, torque balance type
K—Station number 1 watt transmitter, frequency type, from converter
L—Station number 2 megawatts
M—Station number 1 volts
N—Station number 1 megawatts
O—Load injector megawatts
P—System total megawatts
Q—Station number 3 megawatts
R—Load dispatchers total load recorder
S—Load injector
T—Diagram

Conclusions

The new fast responding frequency-type telemeter represents a definite forward step in the art of telemetering by carrier current channels. Although time-

tested basic principles are used, new frequency measuring techniques, new materials, and refinements in well-known principles have resulted in:

1. Instantaneous response, without overshoot.

2. A high degree of accuracy, stability of calibration, and precision.

3. Continuous receiver output signal.

4. Suitability for use with modern power line carrier-current telemetering channels of the usual types.

5. Flexibility of application to a wide variety of telemetering problems.

6. Ease of adjustment and calibration.

7. Absence of contacts and electromechanical relays.

8. Minimum of moving parts.

9. Eminent suitability as a component of automatic control schemes where both high speed and continuous response are required.

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Discussion

E. W. Kenefake (General Electric Company, Syracuse, N. Y.): The author's paper and presentation gives an excellent description of a new telemetering system for carrier-current channels. I would like to add a few additional comments concerning its applicability to carrier-current channels because I believe it is the first major development aimed strongly in this direction.

The important contribution which the authors have made is the use of a telemetering quantity to be transmitted which fits carrier-current channels. This quantity must not require too much band width and be able to cope with variations in the carrier-current channel such as may be caused by carrier attenuation, voltage supply variation, and so forth. Modulation of the magnitude of some quantity such as phase angle, voltage, or pulse width does not fill these requirements nearly as well as a variable frequency.

In regard to band width, practically all carrier channels can accommodate a band width of around 50 cycles without undue hardship. In practice, it would not be advantageous from a carrier-current standpoint to go much lower in frequency but a higher frequency would begin to waste band width. Because the quantity to be transmitted modulates the carrier or a subcarrier, the band width required is twice the highest frequency component that has to be transmitted. Therefore, some low frequency range, such as 6–27 cycles, is preferred.

For example, on narrow band crystal controlled frequency shift equipment with an 0.08 per cent shift in frequency, the band width is 80 cycles at 100 kc; therefore, the limit on the number of channels that can be grouped together is still limited by the carrier current equipment and not by the telemetering scheme. The choice of quantity to transmit, therefore, stems from what is convenient and economical to do with carrier equipment as well as from the characteristics of existing carrier equipment. The same frequency differential could be used at a higher frequency, such as 249–270 cycles, but then the carrier channel would have to provide a band width of 540 cycles as opposed to 54 cycles. Under these assumed conditions, only 1/10 the number of telemetered readings could be transmitted over a given carrier channel.

Another point to emphasize is the use of input and output transformers on the telemetering receiver and transmitter suitable for the 6–27 cycles which enables the system to be coupled properly to various carrier-current equipments quite readily.

Basically, this telemetering system employs frequency modulation because of the variable frequency of 6–27 cycles and the limiting in the receiver, and is the main reason it enjoys such freedom from interference and variation in the transmission channel. From a carrier-current viewpoint, no telemetering system is useful for carrier-current channels unless it can tolerate variations in the transmitting medium.

G. S. Lunge (General Electric Companys Schenectady, N. Y.): Kenefake has pointee out that while this new telemet er dod require a 54-cycle band width, this is within even the extremely narrow band width used by a crystal controlled frequency-shift carrier-current channel, at least for carrier frequencies from 90 to 200 kc. For carrier frequencies in the 30 to 90 kc range, it is necessary to forego the use of crystal control when a frequency shift carrier-current channel is used with this new telemeter. This will seldom prove to be a hardship, because of the reduced line attenuation at these lower frequencies and because it is still possible to handle three such carrier telemeter channels with the same line tuning and coupling equipment as would be needed, for example, for a single-frequency carrier telephone channel.

Most carrier-current telemetering channels of either the frequency-shift or the audio tone-modulated type now in service were designed for impulse transmission, initiated by a single contact in the telemeter transmitter. Obviously, some modifications in such carrier-current or audio tone transmitters and receivers are necessary to adapt them for use with the new telemeter transmitter and receiver described in this paper, which contain no electromechanical relays or contacts.

Performance Test of the Allgemeine Elektricitats Gesellschaft Free-Jet Air-Blast 220-Kv 2,500-Megavolt-Ampere Reclosing Circuit Breaker

ALEXANDER DOVJIKOV
MEMBER AIEE

CLIFFORD C. DIEMOND
MEMBER AIEE

Synopsis: Tests of a representative sample of this circuit breaker were made on the system of the Bonneville Power Administration in order to provide an opportunity for American manufacturers and operating engineers to study the operation of this circuit breaker in interrupting faults and line charging currents. A single pair of interrupting contacts in open air, ease of contacts visual inspection, maintenance and replacement attracted much attention and favorable comment, particularly from operating engineers. The circuit breaker successfully interrupted fault currents in about six cycles (average) as compared with three cycles of fast oil circuit breakers of American manufacture. Interruption of line charging current, although successful, was accompanied by many restrikes. It is hoped that these tests and the analysis of the test data will facilitate development of an American circuit breaker which will retain the favored features of the European prototype but will be faster in interrupting fault currents and practically free from restrikes in interrupting line charging currents.

AT A MEETING of the Technical Industrial Intelligence Division of the Department of Commerce in Washington on July 19, 1945, recent types of German circuit breakers were described by technical consultants who had just returned from Germany. It was recommended to the committee and to representatives of electrical manufacturers and utilities who were present that certain of these 115- and 230-kv circuit breakers be tested in this country.

The system of the Bonneville Power Administration, in connection with its rapidly growing generating capacity and transmission lines, requires a large number of fast reliable circuit breakers of very high interrupting capacity, and therefore, Bonneville Power Administration engineers are interested in circuit breaker developments which permit the elimination of the large amount of oil used in conventional circuit breakers and the necessary oil storage and oil handling equipment. Decreased fire hazard, greater ease of maintenance, rapid reclosing ability, and the possibility of single-pole operation are other factors which resulted in the agreement of the Bonneville Power Administration to test the German circuit breakers.

The purpose of the tests was not only to establish the operational characteristics of this particular circuit breaker, but to provide an opportunity for representatives of the American electrical industry to observe this type of circuit breaker in action and, by analysis of records obtained during the tests, to design an American air-blast circuit breaker which may retain the comparative simplicity of the German prototype and at the same time would be faster (at the most, three cycles), built for higher fault-interrupting capacity, and better adapted for interrupting large charging currents of long, high-voltage transmission lines.

The circuit breaker was first shipped to the General Electric Company's Philadelphia works for inspection and preliminary operating tests under no-load conditions, and then it was installed for testing at the J. D. Ross substation (Vancouver, Wash.) of the Bonneville Power Administration. The tests were made on February 22, 23, and 24, 1947, and, in keeping with the fundamental purpose of the tests they were witnessed by representatives of several circuit breaker manufacturers and by many operating engineers from different parts of the United States.

Paper 48-43, recommended by the AIEE switchgear committee and approved by the AIEE technical program committee for presentation at the AIEE winter general meeting, Pittsburgh, Pa., January 26-30, 1948. Manuscript submitted November 17, 1947; made available for printing December 16, 1947.

ALEXANDER DOVJIKOV is chief of analysis and research unit and CLIFFORD C. DIEMOND is chief of relay unit; both with the engineering division of Bonneville Power Administration, Portland, Ore.

We are pleased to acknowledge the valuable assistance of W. Borgquist, general director of the Royal Board of Waterfalls, Sweden, who supplied us with a copy of an instruction book for this circuit breaker, of H. L. Levinton, E. J. Harrington, S. Fry, and the group of oscillograph operators in preparing the test program and making the tests. The tests were made on a heavily loaded, very extensive system, and the work of the dispatchers under the direction of J. O. Swanson deserves special recognition.

General Information Concerning the AEG Free-Jet, Air-Blast Circuit Breaker

1. CIRCUIT BREAKER NAMEPLATE DATA

NR 611299-300-301
AEG Compressed Gas Circuit Breaker
Type *APF-2507*—220/600 A—50 Cycles
Series 220, Rated Voltage 220 Kv
Rated Current 600 Amperes
Short Time Current for 1 second—20,000 Amperes
Inrush Current —25,000 Amperes
Maximum Interrupting Current—8,500 Amperes
Maximum Interrupting Capacity: 3 poles–2,500 megavolt-amperes
Operating Voltage 220 Kv, at 18-20 Atmospheres
Closing Solenoid—220 Volts (Direct-Current)
Opening Solenoid—220 Volts (Direct-Current)
Short Circuit Trip Solenoid—220 Volts (Direct-Current)
General Plan—Number 0111537
Compressed Air Tank Nameplate—21 atmospheres, 570 liters

2. APPEARANCE

The circuit breaker, Figures 1 and 2, is composed of three poles, each pole consisting of two vertical porcelain columns

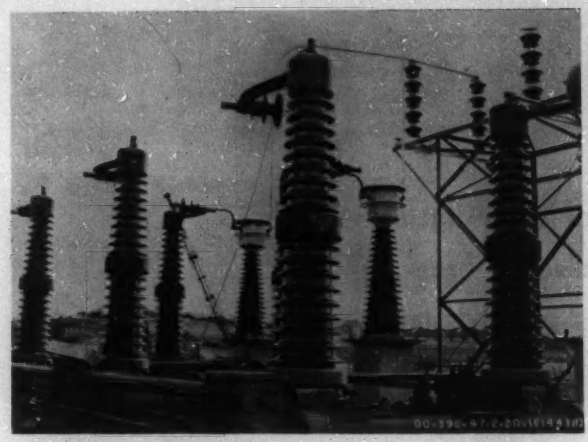


Figure 1. Circuit breaker in open position

Table I. Summary of Contact Millivolt Drop Tests

Contact Drop in Millivolts at 100 Amperes

Phase	On January 24	Immediately Before February 22 Tests	Following Test Number 7	Following Test 12-2
A	17.9	34	23	23.5
B	17	38	18	18
C	18	39	30	22

about ten feet high and separated by a distance of $8^3/_4$ feet. Each column has a radial disconnecting arm attached at its top. All columns are linked at the base in such a way that rotation of the columns causes the two disconnect arms to swing apart for circuit isolation. Each pole unit is mounted on a steel frame directly above a compressed air tank which is an integral part of the frame. The three compressed air tanks are connected together and to the source of high-pressure air. The normal operating pressure is between 18 and 20 atmospheres, or about 280 pounds per square inch. An operating piston mounted on the center phase air tank rotates all insulator columns simultaneously (through linkages) thereby providing opening and closing of the disconnect arms.

Perpendicular to the tip of each radial disconnect arm and extending horizontally is a retractable interruptor contact mounted in an arc extinguishing head, (Figure 3 and Figure 4). The arcing contacts are hollow cylindrical rods which protrude from the extinguishing heads into free air where they are butted together by spring action when in the closed position. High-pressure air is carried up through steatite insulating tubes inside the vertical porcelain columns and out through ducts to the extinguishing heads where it acts both to separate the contacts and to extinguish the arc. When retracted, the contact faces are shielded by a hard rubber nozzle on the extinguishing head which controls the arc and directs the arc-extinguishing air during interruption. The terminal connections are located at the top of each porcelain column and are connected directly to each disconnect arm by means of a slip joint. To prevent the condensation of water inside the insulator columns, a small quantity of low pressure, dry air is circulated continuously in and around the steatite tube in the column.

3. Control System

Figure 5 shows the schematic of the pneumatic control system, and Figure 2 represents the isometric drawing of the circuit breaker with its air system. Other than functions of the tripping and closing solenoids and various mechanical interlocks, all operations are performed pneumatically with pneumatic seal-in until completion of the circuit breaker operation. The nominal operating pressure of the circuit breaker air system is 285 pounds per square inch (20 atmosphere). Minimum operating pressure is 256 pounds per square inch as indicated on the circuit breaker and by drop out of a low-pressure relay located in the circuit breaker control circuits. In order to overcome the toggle and buffer springs, a minimum of about 180 pounds per square inch is necessary for a 3-phase full-open operation. The circuit breaker requires 175 pounds per square inch to completely close from the full-open position. Interrupter contacts retract at a minimum of 60 pounds per square inch.

A. *Three-pole closing* from the full-open position of the disconnect arms may be initiated by either energizing the "close solenoid" or by manually applying high-pressure air from the main reservoir to the close holding piston through an external hand-operated valve (not shown). Motion of the solenoid plunger is transmitted through a linkage to open the "close valve," which permits the high-pressure air to flow from the air reservoir to the main operating piston. The insulator columns and disconnect arms are caused to rotate until full closure is obtained. No operation of the blast valves and interruptor retractable contacts occurs during closing.

Seal-in, until complete closure, is obtained by introducing air from the downstream side of the close valve to the "close" holding piston. Seal-in is continued until full closure of the circuit breaker when the disconnect operated rocker arm opens the ball valve below the "close" holding piston. Solenoid current is interrupted by mechanical auxiliary switches.

A typical closing time as measured from coil energization was 36 cycles (0.6 second) with an accompanying drop in air pressure of about 5 from 285 pounds per square inch.

Air remaining in the main operating piston and line is exhausted to the atmosphere through ports in the circumference of the piston cylinder.

B. *Three-pole, full-open tripping* is similar in initiation and seal-in to the corresponding features of the closing operation.

Air from the full-open trip valve is fed directly to the single-phase blast valves on the outer phases and through the opening piston control valve to the blast valve on the center phase. This air causes the blast valve main piston to open a valve which permits the passage of high-pressure air directly from the main reservoirs to the interrupting heads on each phase. As the central phase blast valve main piston moves down, a port is uncovered which allows the air to flow back to the top of the opening piston control valve.

This causes the opening piston control valve to open to admit air to the main operating piston. The foregoing sequence phase interruptors provide interruption of current before disconnect rotation is started by the main operating piston. After about 30 degrees of rotation, the disconnect-operated cam and its associated interlock piston cut off the air from the opening piston control valve causing the blast valve to reset, thus cutting the high-pressure air from the interruptor heads. Any high-pressure air left in the blast valve is quickly exhausted to atmosphere through the air relief valve as the blast valve piston resets.

Typical times for this type of operation varied from 7.5–8.0 cycles as measured from energization of the solenoid to the time of contact parting. The corresponding pressure reduction averaged 35 pounds per square inch for this 3-phase operation. The disconnects were fully open in about 35–40 cycles.

C. *The single-phase, short-circuit tripping* is arranged to provide high-speed, single-phase or 3-phase reclosing.

Operation of the corresponding trip solenoid as determined by the external phase selecting protective relays admits air from the reservoir to the holding piston, the pneumatic delay chamber, and the single-phase blast valve of the appropriate phase or phases. The blast valve function is similar to that previously covered except that air is not fed back to the opening piston control valve to cause disconnect rotation. The interruptor contacts are held in the retracted position by air from the blast valve for a definite period. This period is determined by the length of time required to equalize pressures on both sides of the holding piston. Equalization takes place through a small reducer and the pneumatic delay chamber.

Contact parting time varied from 3.5 to 3.7 cycles with a pressure reduction of about 6 pounds per square inch for a single reclosing operation of one phase. The contacts remain open for a minimum of about 11 cycles before reclosing. Reducers were prepared which would permit reclosing in about 15, 20, and 25 cycles, as measured from trip coil energization to the time at which the contacts first touched on reclose. Actually during the reclosing fault tests, the circuit was reestablished by arcing about two or three cycles before the contacts touched.

Provision is made for interruption of solenoid current by air-operated auxiliary switches.

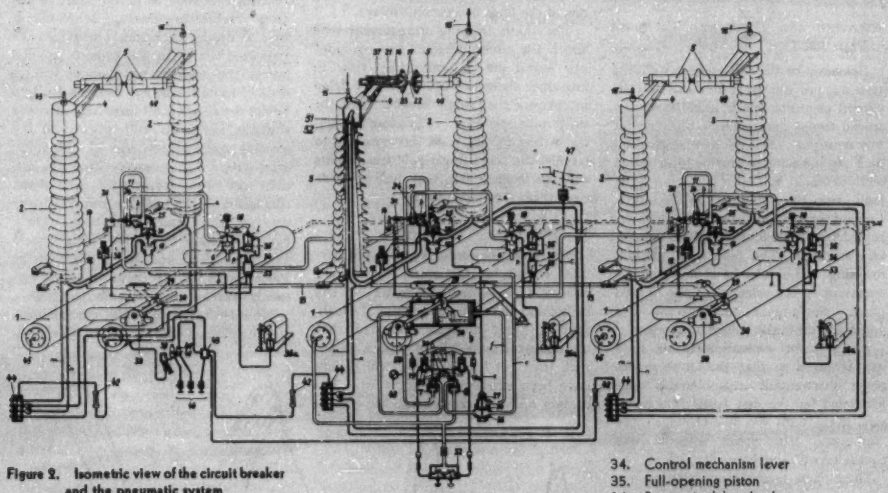


Figure 2. Isometric view of the circuit breaker and the pneumatic system

1. Compressed air tank
2. Rotating insulator column
3. Insulating pipe
4. Disconnect supporting arm
5. Arc extinguishing head
6. Short-circuit trip valve, single phase
7. Closing valve
8. Full-opening valve, 3 phase
9. Compressed-air operating mechanism, 3 phase
10. Blast valve, single phase
11. Blast valve control mechanism
12. Blast air pipe
13. Interphase linkage
14. Intrapole cross linkage
15. High voltage terminal
16. Sliding contact
17. Retractable interruptor contact
18. Short-circuit trip valve operating coil
19. Full-opening trip valve operating coil
19a. Closing valve operating coil
20. Blast valve operating piston
21. Interruptor contact piston
22. Opening in extinguishing nozzle
23. Extinguishing nozzle
24. Blast valve control piston
25. Sliding control valve
26. Opening-piston control valve
27. Valve piston
28. Valve disk
29. Cam roller
30. Cam
31. Cam linkage
32. Manual operation control valve
33. Holding piston
34. Control mechanism lever
35. Full-opening piston
36. Pneumatic delay chamber
36a. Air-operated auxiliary switch
37. Pressure spring
38. Air relief valve
39. Ventilating valve
40. Reducing valve
41. Oil separator
42. Low-pressure air volume meter
43. Main low-pressure air manifold
44. Low-pressure air distribution manifold
45. Water drain plug
46. Oil drain petcock
47. Tank pressure supervisory switch
48. Pressure gage
49. Water drain
50. Speed regulator
51. Check valve
52. Check valve orifice
53. Reducer

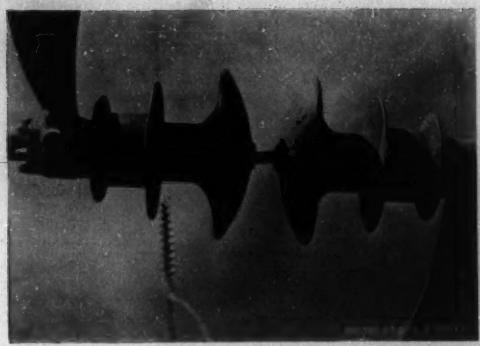


Figure 3. Interrupting head

Relaying and Electrical Controls Used for Test

Relaying for the single-phase reclosing tests was provided by instantaneous overcurrent elements, one for each phase. A special timing and auxiliary relay circuit was arranged. This circuit energized all four trip solenoids to provide blast action and disconnect full open operation if the fault was re-established on reclosure. Current for the four solenoids was interrupted by circuit breaker operated auxiliary switches. The first fault for any reclosing test would be interrupted by operation of the solenoid of the faulted phase, single-phase short-circuit trip valve, and the faulted phase blast valve.

Relaying for 3-phase reclosing tests was arranged so that the three single-phase short-circuit trip solenoids were paralleled for the first fault. On reclosure, if the fault was re-established, the "full-open" trip circuit solenoid was paralleled with the three solenoids.

Three-phase nonreclosing relaying was arranged so that all four trip solenoids were energized simultaneously by any one of the fault overcurrent relays.

The 3-pole tripping arrangement with which the circuit breaker was equipped was found during preliminary tests to introduce about two cycles of more delay in opening the circuit breaker than the single-pole operation. Therefore the control wiring for the tests was arranged to parallel the three single-pole trip circuits and the 3-phase trip coil during 3-pole circuit breaker operations.

4. Retractable Contacts

The retracting contacts of the AEG air-blast circuit breaker are hollow copper rods with an outside diameter of $1^1/_8$ inches and an inside diameter of 5/8 inch. Spectrographic analysis indicated that the tips were predominantly pure copper with traces of lead and tin (which may have been extraneous impurities). The contact rods are screwed into the operating piston assemblies and may be easily interchanged.

During the lower fault-current tests on the circuit breaker, the outer edges of the tips were found to have slightly mushroomed out due to impact during circuit breaker closing operations. This swedging action had increased the tip radius by approximately 1/16 inch after test 11, at which time they were dressed down.

As the fault currents increased, the inner surface of the tips began to assume a funnel shape presumably caused by the hot arc, the products of which were blown back through the contact rod to the baffle plate at the rear end of the interruptor. Examination of this baffle after the test showed traces of molten copper deposited on the baffle plate.

In spite of the distortion of the contact tips the conductivity between the contacts remained as good as at the start of the test. Preliminary checking of the circuit breaker during mechanical operating tests indicated that with 100 amperes applied across the contacts an average voltage drop of 18 millivolts was measured. Just before the actual fault tests (and after the contacts had been well lubricated) the average voltage drop was 37 millivolts; after test 7 the average drop was 20; after test 16 the drop was 21; and at the conclusion of the tests the millivolt drop was of the same magnitude. These figures compare favorably with typical values of 25–35 and 40–50 millivolts at 100 amperes for two types of 230-kv circuit breakers of American manufacture.

Since the later high-fault current tests were conducted only on the *A*-phase pole,

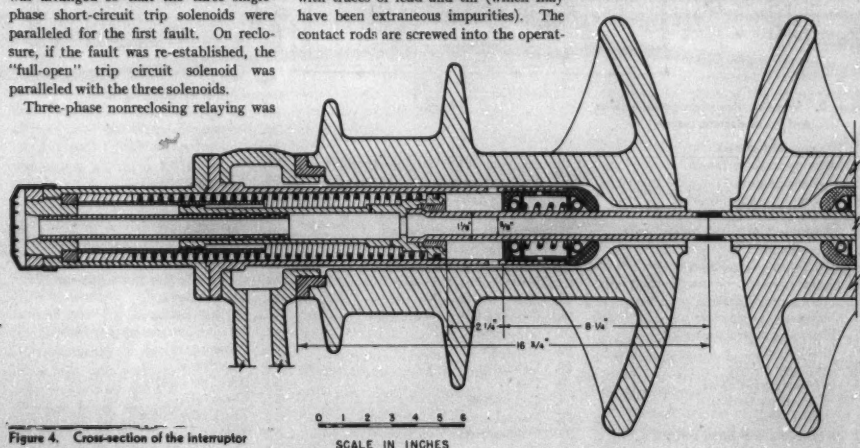


Figure 4. Cross-section of the interruptor

the duty on the A-phase contacts was relieved by the interchange of the C-phase contacts following test 31, at which time the contacts were again dressed down. This operation took less than 30 minutes. See Table I.

General Test Program

1. Fault-current interrupting tests were made to determine the ability of the circuit breaker to interrupt 3-phase-to-ground and single-phase-to-ground fault currents within its rating, and the time required for interruption. The effect of air pressure on the ability of the circuit breaker to interrupt fault currents and on the speed of interruption was also covered within the permissible range of operating pressures. The tests were started with currents approximating 20 per cent of the circuit breaker rating, and the current was increased in steps as permitted by system connections; the maximum current value being limited by the system capacity at the test location.

2. Reclosing tests were made to determine the suitability of the circuit breaker for both 3-pole and single-pole, high-speed reclosing duty especially for 20-cycle or faster reclosing. These tests also started with lower current values which increased as the tests progressed. Determination of the fault-arc deionizing time for the various current values was considered to be an important by-product of these tests inasmuch as the deionization time determines how rapidly reclosing may be permitted to take place without re-establishing the fault arc for nonpermanent faults.

3. Line-charging current switching tests were made to determine the suitability of the circuit breaker for this service. Particular attention was given to restriking and to overvoltages produced by such restriking. The effect of current magnitude was covered by using two transmission lines having lengths of 71 miles and 169 miles, respectively.

Measurements

The various quantities affecting the performance of the circuit breaker were recorded by means of electromagnetic oscillographs. To obtain a more detailed voltage record (and to check voltage records on the electromagnetic oscillographs) voltage of one phase was recorded by photographing on moving film the screen of a sealed cathode-ray tube. In addition, a Western Electric Fastax camera capable of taking 16 millimeter motion pictures at 4,000 frames per second was used to photograph the operation of the AEG circuit breaker. Projection of the pictures at the normal speed of 16 frames per second produces slow-motion pictures with time expansion of 4,000/16 or 250 times.

Results of the Fault Current Interrupting Tests

The data obtained from the oscillograms are summarized in Tables II and III. These show that, with line-to-line bus voltages of 207 to 210 rms kilovolts, the circuit breaker interrupted fault currents in from 3.8 to 6.5 cycles with the majority of the faults being interrupted in from 5 to 6 cycles. Figure 6 represents a typical oscillogram of opening 1,300 megavolt-amperes of the 3-phase-to-ground solid fault. The circuit breaker successfully interrupted all faults except during one test (number 5) covering a 3-phase arc fault with 15-cycle (American Standards Association, ASA, definition) reclosure. For this test, phase C failed to interrupt during the second interrupting operation and the fault was cleared by the backup circuit breaker. No reason is known for the failure of the circuit breaker to interrupt fault current on this operation.

No noticeable difference was observed between the first and second operation during reclosing tests. When interrupting the higher value of fault currents, the contact arc exhibited a more flaming characteristic. Examination of the high-speed motion pictures taken during these tests corroborated this visual observation and disclosed some tendency of the arc to flare radially for distances approximating the diameter of the arc head. It is of interest to note that the heavier fault currents required somewhat less time to interrupt than the lighter values of fault current. For fault values ranging from 1,600 to 2,100 megavolt-amperes, the circuit breaker interrupted within 5.4 cycles except for test 29 (a close-open operation at 2,100 megavolt-amperes) which required 5.8 cycles from trip energization to interruption.

The 3-pole arc fault reclosing tests show two unsuccessful reclosures (fault arc re-established after reclosure) where the dead time between the end of the first interruption and the re-establishment of the cir-

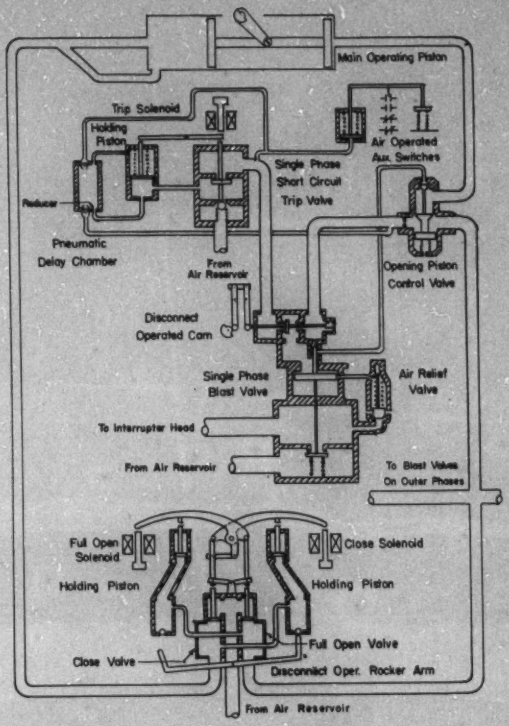


Figure 5. Pneumatic system schematic, center phase

Table II. Summary of Results of Fault Tests on a 220-Kv, AEG, Free-Stream, Air Blast Circuit Breaker

J. D. Ross Substation—Bonneville Power Administration—February 22–24, 1947

Test	Type of Fault	Circuit Breaker Duty[1]	Air Pressure Before	Air Pressure After	Time from Trip Coil Energization to Arc Interruption[2] 1st Interruption A	B	C	2nd Interruption A	B	C	Dead Time Between End of 1st Interruption and Start of 2nd Current Flow A	B	C	Back-up Circuit Breaker Trip[7]	Fault Current[4] 1st Interruption A	B	C	2nd Interruption A	B	C	Bus Voltage Kv	Fault Megavolt-Amperes[5] 1st Interruption	2nd Interruption	Test	Notes
1	3 Phase to ground	F.O.	300	260	5.4	5.3	5.1								1,350	1,350	1,350				209	500		1	
2-1	3 Phase to ground	F.O.	285	243	5.2	5.3	5.1								1,400	?	?				210	?		2-1	5,11
2-2	3 Phase to ground	F.O.	285	245	5.8	5.9	5.9								1,400	1,400	1,350				210	500		2-2	
2-3	3 Phase to ground	C-F.O.	285	230	5.6	5.9	5.7								1,400	1,400	1,350				210	500		2-3	
3	3 Phase to ground	F.O.	270	230	5.5	5.8	6.1								1,350	1,400	1,400				210	500		3	
4	Arc, 3 phase to ground	0–20 cy-C	285	245	5.1	6.0	5.8				13.7	12.0	12.5		1,350	1,350	1,350				210	500		4	13
5	Arc, 3 phase to ground	0–15 cy-C-F.O.	285	225	6.3	6.1	6.4	5.2	5.4	16.3	5.5	6.0	5.4		1,350	1,350	1,350	1,400	1,350	1,350	209	500	500	5	13
6	Arc, 3 phase to ground	0–15 cy-C-F.O.	300	235	6.5	5.9	5.2	5.4	5.7	5.5	5.5	5.9	6.9		1,350	1,350	1,350	1,350	1,350	1,350	210	500	500	6	14
7-1	3 Phase to ground	F.O.	300	250	5.1	5.4	5.2								3,600	3,500	3,600				209	1,300		7-1	
7-2	3 Phase to ground	F.O.	285	235	5.2	5.5	5.4								3,700	3,500	3,000[9]				210	1,350		7-2	
8	Arc, 3 phase to ground	0–20 cy-C	300	265	5.4	5.7	5.6				?	?	?		3,700	?	?				210	1,350		8	5,6,13
9-1	1 Phase to ground	F.O.	285	235	5.1										4,000						207	1,450		9-1	
9-2	Line arc, 1 phase to ground	0–20 cy-C-F.O.	300	248	5.1			4.7			13.7[10]				3,800			3,900			207	1,350	1,400	9-2	14
10	Fuse	See Note 6			3.8										3,800						209	1,400		10	11
11	Fuse	See Note 6			14.9										4,700						209	1,700		11	16
12-1	1 Phase to ground	0–20 cy-C-F.O.	285	230	5.2			5.4			14.6			6.2	4,400			4,600			209	1,600	1,650	12-1	
12-2	Line arc, 1 phase to ground	0–25 cy-C	285	275	5.2						17.9[10]				4,600						210	1,700		12-2	12
13	Fuse	See Note 6					3.5											4,500			210	1,650		13	
14	1 Phase to ground	0–25 cy-C-F.O.	285	225	5.3			4.6			17.7			1.9	5,800			5,800			210	2,100	2,100	14	17
15	1 Phase to ground	0–25 cy-C-F.O.	285	225	5.0			5.2			18.0			4.4	5,800			5,800			210	2,100	2,100	15	
16	Fuse	See Note 6					2.4											6,600			210	2,400		16	
29	1 Phase to ground	C-F.O.	285	227	5.8										5,800						209	2,100		29	
30	1 Phase to ground	0–20 cy-C-F.O.	285	225	5.1			5.2			12.6			7.6	5,000[9]			5,800			210	1,800[9]	2,100	30	
31	1 Phase to ground	0–20 cy-C-F.O.	285	225	5.1			5.3			13.8			4.1	5,800			5,800			209	2,100	2,100	31	
32	Line arc, 1 phase to ground	0–25 cy-C	285	265	5.4						17.9[10]				4,600						209	1,650		32	12
34	Bus arc, 1 phase to ground	0–25 cy-C	285	275	5.1						?				5,700						210	2,100		34	13

Notes:

1. Reclosing time is nominal time from first trip coil energization to contact touch on reclosing.
2. All times in cycles on 60 cycles-per-second base.
3. Time from second trip coil energization to tripping of backup circuit breaker (none taken before Test 11).
4. Rms fault current $= \sqrt{AC^2 + DC^2}$; taken for circuit breaker at 3.6 cycles after trip coil energization (approximate time of contact parting); taken for fuse at start of arcing.
5. Fault megavolt-ampere $= \sqrt{3}$ (bus voltage) (Highest phase-current).
6. Melting link for fuses was cut; therefore interrupting time does not include time of melting link.
7. Current values not obtained—No "A" oscillogram.
8. Dead time indeterminable from oscillogram.
9. Low value attributed to current-transformer saturation.
10. This period includes a period of capacitive-coupled current flow. See discussion for this test in results.
11. Restrike on *A* Phase.
12. Successful reclosure.
13. *C*-phase failed to open on second interruption, *B*-phase restruck for one-half cycle on second interruption. Unsuccessful reclosure.
14. Unsuccessful reclosure.
15. Unsuccessful reclosure. Backup circuit breaker cleared second fault.
16. Fuse failed to clear fault. Current fully offset.
17. Backup circuit breaker cleared second fault.

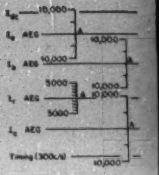
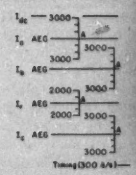


Figure 6. Test 7-1: Solid fault 3-phase-to-ground at Ross substation. Operation: open. Calibration in instantaneous values

cuit was of the order of only six cycles (tests numbers 5 and 6, Figure 7). Two additional tests, numbers 4 and 8, having a dead time of the order of 12 to 14 cycles resulted in successful reclosure indicating that the minimum possible deionizing time of a fault arc across a 16-unit insulator string may range between the values of 8 and 12 cycles. The single-pole reclosing tests are believed to be even more interesting. In the majority of these tests a 71-mile transmission line was energized from the Ross Substation with the fault applied to one conductor of the single-circuit transmission line. Operation of the air-blast circuit breaker served to disconnect only the faulted phase from the Ross bus. This phase, however, remained capacitively coupled to the two energized unfaulted phases. Unfortunately, the current transformer which had been connected in the fault circuit, to show the fault current continuity as a consequence of the capacitive-coupling current after the circuit breaker operation, was flashed over by the fault arc, thereby effectively short circuiting it so that records of the coupling capacitive arc current were not obtained. However, a second indication of the presence of such capacitive current was provided by the high deflection galvanometer connected to record the voltage across the arc path. This galvanometer showed a changing voltage during the periods of capacitive coupling current in accord with the inverse current-voltage law of arcs in a gas; in addition this voltage is nonsinusoidal during the passage of the capacitive coupling current through the fault arc path and becomes sinusoidal with the cessation of such current flow. In test 9-2 (Figure 8), the 13.7 cycles between the end of the first interruption and the re-establishment of the circuit consisted of approximately 11.2 cycles of capacitance follow current and of approximately 2.5 cycles of time during which no current flow occurred. At this point the circuit breaker contacts were reclosed, and the fault path withstood normal voltage for approximately 0.1 cycle (as shown on both the electromagnetic and cathode-ray oscillograms) following which the system voltage reached a point sufficient to break down the inadequately deionized arc path to re-establish the fault. During the remaining single-pole reclosing tests the time between the interruption and reclosure was increased approximately 18 cycles, and three tests with successful reclosure were performed.

Figure 7. Test 6: Phase-fault-to-ground at Chehalis. Operation: Open-close-full open. Calibration in instantaneous values



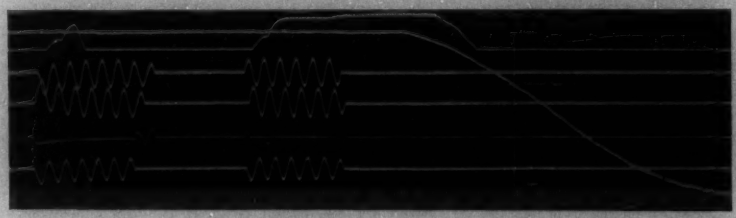


Table III. Summary of Results on Line-Charging Current Interrupting Tests

Test Number	System Kv	Cycles of Restriking	Number of Restrikes After 1/4 Cycle of Zero Current: Phase A	B	C	Current Flow After Disconnect Movement: Restrikes Phase A	B	C	Approx Cycles Phase A	B	C	Voltages in Times Normal Crest Voltage: Magnetic Oscillograph Phase A	B	C	Cathode-Ray Oscillograph Phase C	Phase C Neglecting Short-Time Peaks
17	210	5	0	6	8*	x	x	x	x	x	x	1.6	2.4	2.3	x	x
18-2	209	7	10	8**	14	0	0	4	0	0	2	2.2	1.8	2.3	2.8	2.5
23-2	207	7.5	12	5	13	3	0	5	1.5	0	2.5	2.5	2.4	2.4	2.9	2.6

x No "A" Oscillogram.

* Approximately 1 1/2 cycles of zero current before first restrike.

** Approximately 2 1/2 cycles of zero current between first and second restrikes.

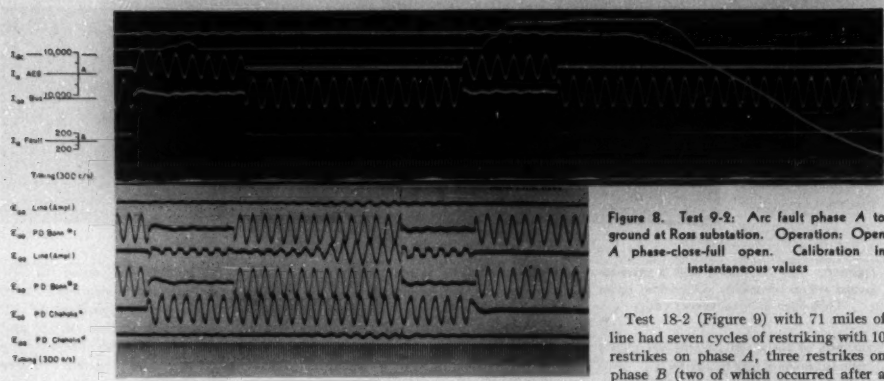


Figure 8. Test 9-2: Arc fault phase *A* to ground at Ross substation. Operation: Open *A* phase-close-full open. Calibration in instantaneous values

These values indicate that, at least for the circuit conditions used, 14 cycles between interruption and reclosure is insufficient to ensure that the arc path deionization will proceed to a point adequate for withstanding the system voltage; whereas 18 cycles provided the necessary additional time for the path to deionize sufficiently. The high-speed motion pictures definitely show the presence of current in the arc path following circuit breaker opening, a thin-cored arc being visible after the cessation of the main fault current within the luminous gasses which had been produced by the main fault arc. The pictures for test number 9-2 shows the continuous presence of this thin-cored arc between the end of the first fault current interruption and the re-establishment of fault current by the circuit breaker reclosure. Although the few tests which were made are not considered conclusive, they confirm data obtained by the Bonneville Power Administration from similar tests at lower fault current values.

Although the Bonneville Power Administration in some previous tests of single-pole reclosing has successfully reclosed 230-kv circuit breakers in approximately 12 cycles between interruption and reclosure, the duration of the capacitive-coupled current was of the same general order as during this series of tests. Consequently, pending the acquisition of more data, it is our belief that where single-pole reclosing is employed, for power fault currents ranging between 600 and 5,000 amperes and for conventional 230-kv lines, a minimum of 17-20 cycles must be allowed between the interruption and reclosure to ensure successful reclosure. The difference in the time between interruption and reclosure of 17-20 cycles for single-pole reclosure and 8-12 cycles for 3-pole reclosure is due to the effects of the capacitively-coupled current encountered in single-pole reclosure.

The low deflections necessitated by the narrow film used with the cathode-ray oscillograph was not conducive to accurate determination of the rate of rise of the recovery voltage following a fault. Measurements under similar test conditions give values ranging as low as 200 volts per microsecond and as high as 1,000 volts per microsecond; the differences were seemingly caused by the instantaneous value of voltage toward which the system was rising at the moment of interruption. The majority of measurements of rates of rise of recovery voltage resulted in values close to 450 volts per microsecond. The results of the fault tests are summarized in Table II.

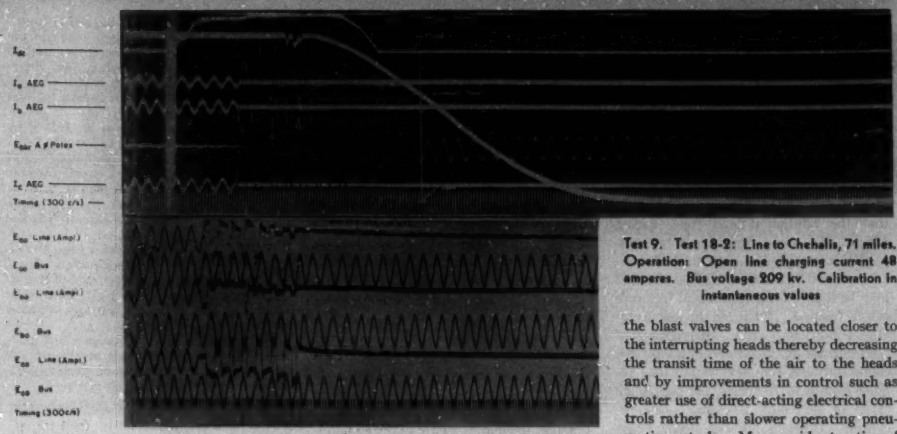
Results of the Line-Charging Current Interruption Tests

The interruption of line-charging current by the AEG circuit breaker resulted in numerous "restrikes" at the half-cycle points both with the 71-mile line and the 169-mile line. Restrikes were not counted prior to a zero current period of at least one-quarter cycle, all previous current discontinuities being classed as arcing. Furthermore, the restriking was not restricted to the period normally ascribed to current interruption by the circuit breaker, that is, during the time of operation of the blast valve and retraction of the contacts within the interrupting heads, but continued for periods of column rotation for isolation up to approximately nine per cent of rotation.

Due to the large number of restrikes at the half-cycle points, only three such tests were made; two tests on the 71-mile line and one test for the 169-mile line.

Test 18-2 (Figure 9) with 71 miles of line had seven cycles of restriking with 10 restrikes on phase *A*, three restrikes on phase *B* (two of which occurred after a lapse of $2^1/_2$ cycles), and 14 restrikes on phase *C*. Four of the phase *C* restrikes occurred after the columns started rotating so that about 6 per cent isolation was achieved before line-charging current interruption was completed. The maximum line overvoltage was produced at the next to the last half cycle. It was measured as 2.8 times normal on the cathode-ray oscillograph record and only 2.3 times normal on the magnetic oscillograph record. This discrepancy was naturally expected.

Test 23-2 with 169 miles of line had seven and one-half cycles of restriking with 12 restrikes on phase *A*, of which three occurred during disconnect movement amounting to about five per cent of its travel, five restrikes on phase *B* before disconnect movement, and 13 restrikes on phase *C*, of which five restrikes occurred during disconnect movement amounting to about nine per cent of its travel at the end of which the retracted contacts were separated by a distance of 20 inches. A potential transformer connected to phase *B* at North Bonneville substation, 36 miles out on the line being switched, is believed to have been of assistance to the phase *B* interruption by draining a portion of the charge from the phase-*B* line conductor so that restriking on this phase took place for only $2^1/_2$ cycles. A slight indication of low-frequency oscillation such as takes place between line capacitance and potential transformer inductance which saturates in the presence of the line charge is shown on the line voltage record for phase *B*. Maximum overvoltages during this test were measured on the cathode-ray oscillograph connected to record the phase-*C* voltage to ground of the line as 2.9 times normal and on the electromagnetic oscillograph as 2.5 times normal on the next to the last restrike for phase *C*.



Test 9. Test 18-2: Line to Chehalis, 71 miles. Operation: Open line charging current 48 amperes. Bus voltage 209 kv. Calibration in instantaneous values

The high-speed motion pictures corroborate the movement of the isolating disconnects before current interruption during test 23-2. This was the only test showing this effect for the phase-*A* pole of the circuit breaker, the pole which had been chosen for photographing.

Oscillograms also were taken during two line energization tests. No unusual phenomenon or overvoltage are shown on the records. Likewise, during all the line-switching tests, disturbance to the bus voltage was practically zero. The results of line-charging current interruptions are summarized in Table III.

Discussion and Conclusions

Examination of the results shows that the tested AEG circuit breaker interrupts 60-cycle fault currents within its rating in from 5 to 6.5 cycles (0.08 to 0.108 second). Preliminary tests showed that close to 3.6 cycles (0.06 second) were required to part the contacts leaving an arcing time of 1.4 to 2.9 cycles.

The question arose as to the evaluation of the nameplate interrupting capacity of the tested circuit breaker (2,500 megavolt-amperes). According to the German standards (VDE Std. number 0670, November 1941, Par. number 39):

"The nominal interrupting capability of the circuit breaker in which the minimum time between the trip impulse and the separation of contacts is equal or longer than 0.1 of a second must be proved only on the basis of symmetrical interrupted current. In such cases the unidirectional component of the short-circuit current at the moment of contact separation must not exceed 20 per cent of the peak value of the a-c component.

"For circuit breakers in which the time is shorter than 0.1 of a second, the nominal interrupting capability must be proved also on the basis of asymmetrical interrupted current. In this case the unidirectional component of one phase must be at least 50 per cent of the peak value of the a-c component."

In departure of this rule, however, the calculations of interrupted megavolt-amperes in this report were made in conformity with the present ASA Standards, that is, on the basis of the measured asymmetrical value of current at the moment of contact separation. The reasons for this method of calculation are:

1. The tests were made to determine the characteristics of the circuit breaker when used on an American system and therefore a common basis of comparison with an American-made circuit breaker should be used, and the use of the ASA definition would result in data of greater use to American engineers.
2. In the various tests the times of interruption were both greater and less than the 0.1-second demarkation time of the German standards covering the effect of asymmetry.
3. The tests were made at 60 cycles, whereas the circuit breaker has a 50-cycle rating.

Although the time required for 2-fault interrupting operations at 500 megavolt-amperes was longer than 6 cycles, the circuit breaker in its present form is believed to be a 6-cycle (0.1 second) circuit breaker with 0.06 second required to part contacts (dead time) and 0.04 second of arc interruption (arcing time). Both components of the total interrupting time are longer than in the fastest circuit breakers of American manufacture.

Shorter dead time appears possible if the blast valves can be located closer to the interrupting heads thereby decreasing the transit time of the air to the heads and by improvements in control such as greater use of direct-acting electrical controls rather than slower operating pneumatic controls. More rapid retraction of the contacts into the interrupting heads also may be possible. A dead time of 1.5 to 2.0 cycles or even shorter may be feasible.

Improvement in arcing time may be more difficult to obtain, as the contacts must be retracted from a region of atmospheric pressure to the pressure conditions present within the interrupter heads. Inspection of the construction of the interrupter heads leads to the question of whether the interrupter construction results in the arc being drawn into a region of high-pressure air. Such action would be very desirable in order to use the excellent insulating property of high-pressure air. Better interrupter-head design, making this possible, and possibly more rapid contact movement should result in a considerable decrease in arcing time and a smaller deviation than the 1.5 cycles found in the arcing time required during the tests. The characteristics of the circuit breaker while interrupting line-charging current are considered particularly poor in view of the comparatively low-line voltage (207 to 210 kv) of the tests. The lengthy restriking period at the half-cycle points, while not producing serious overvoltages during the tests, is considered objectionable. The difficulty of the circuit breaker to interrupt line-charging current, as shown by disconnect operation before interruption coupled with the restrikes recorded on the oscillograms obtained lead to the opinion that such interruptions are tantamount to a plain break in air. Again this points to possible improvement of interrupter-head design to obtain the insulating qualities of the high-pressure air available with air-blast circuit breakers.

The limitation of overvoltages during line-charging current interruption tests to less than three times normal is attributed in part to corona effects which quickly lowered the trapped voltage on the line being switched as is shown on the line voltage traces of the line-drop test oscillograms.

The data obtained on arc deionization are believed to be of considerable importance. For 3-pole reclosing it indicates that the minimum dead time, during which fault arc current must be completely interrupted at both ends of a circuit, cannot be less than eight cycles and may be longer before successful reclosure can be assured. Even for the less favorable conditions, where interruption of fault current at the far end of a long line connected to a complex system may require from 8 to 10 cycles, the possibility exists of successful 20-cycle, 3-pole reclosure. However, if low-ohmic series resistors are included in the construction of a circuit breaker, the total time of resistor current, both immediately following the opening of the circuit breaker and immediately preceding its reclosure, must be added to the minimum permissible fault deionization time in order to ensure successful reclosure. Nonsimultaneous relaying on the longer lines will also serve to lengthen the permissible minimum reclosing time.

Single-pole reclosing was shown in the results to probably require from 17 to 20 cycles for arc deionization alone because of capacitive coupling of the faulted phase with the unfaulted phases. Here again the presence of series resistors in the circuit breaker may necessitate a further increase in time to allow for the periods of resistor current. Since both the coupled current and the resistor current are small compared to the fault current, and since for at least a portion of the period between interruption and reclosure they flow simultaneously, the total time of resistor current flow may not add directly to the 17-20-cycle deionization time believed possible for circuit breakers without resistors.

In light of the foregoing, we consider present circuit breaker standards to be insufficient for defining the interrupting time of circuit breakers equipped with low-ohmic resistors. We urge that additional data be secured whenever possible to more conclusively establish the deionizing time of power fault arcs.

The AEG air-blast circuit breaker does not approach the best of American oil circuit breakers in speed of fault-current interruption, in the ability to interrupt line-charging current with little or no restriking. However, the simplicity of the AEG circuit breaker, its ease of maintenance, freedom from use of oil, oil maintenance and handling requirements, freedom from fire, and its general performance evoked wide-spread interest among all observers. It is believed that its controls and the interrupting heads can be modified to approach American standards without sacrificing basic simplicity, and other advantages of the AEG circuit breaker. In order to clarify the necessary modifications, additional tests are considered necessary.

Some of the Proposed Additional Tests

1. One of the most interesting features of the tested circuit breaker is that even for 220 kv a single break is employed, whereas the designs of other manufacturers use a multibreak principle. It is considered desirable to test this entire circuit breaker as a single-pole circuit breaker with two and three poles of the circuit breaker in series to represent multibreak action and to establish in this way any advantages that multibreak may have over a single break in the speed of fault-current interruption and in decreasing restriking during the interruption of line-charging current. The use of resistors with the circuit breaker should also be investigated.
2. As the maximum faults at the J. D. Ross substation were limited to about 2,100 megavolt-amperes (ASA definition) and were cleared by the tested circuit breaker without difficulty, it is intended to test the circuit breaker at Grand Coulee where heavier faults, well above circuit breaker rating, may be established.
3. Testing this circuit breaker from an isolated Grand Coulee bus (with no lines connected) will provide a higher rate of rise of recovery voltage than at J. D. Ross substation. For the same purpose, 3-phase faults will be ungrounded.
4. It is intended to investigate the possible improvement in circuit breaker operation resulting from the use of special alloys for the tips of the retractable contacts.

These proposed tests by the Bonneville Power Administration have been indefinitely deferred because of budgetary limitations.

This circuit breaker in its present form will be installed in the Bonneville Power network for observation of its characteristics in actual service.

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Discussion

R. Willheim (201, Clive Road, London, S.E. 21, England): The presentation of the design which is the subject of the present paper has, from a formal aspect, an unusual feature. It is a report on field tests performed on a circuit breaker not submitted by its manufacturer, and carried out without competent representatives of the firm having a chance of tendering advice. The authors must be complimented on the very fair and tactful approach and the most efficient way in which they have discharged their delicate and difficult task. The writer feels entitled to these comments in view of the fact that the first version of the 230-kv free-jet circuit breaker was developed before 1937 at which time he resigned from his post as chief electrical engineer of the section of the AEG works manufacturing high-tension equipment. Out of the old team he would appear to be the only senior member living outside Germany. He thinks that by contributing to a number of points raised by the authors he may achieve the double purpose of serving the art and enhancing the recognition due to the pioneers of a new technique.

The first 110-kv air-blast circuit-breaker was completed in 1929, and the first 220-kv type was commissioned in 1934. Using a single break up to 110-kv inclusive, the "candle types" in which these early efforts crystallized were characterized by a closed arc chamber incorporating a metal nozzle of large diameter, $1^1/_4$ inches. As soon as the tip of the tapered pin at the end of the contact rod was withdrawn, it immediately passed into the zone of most effective blast. At the same time, a suitable clearance from the large surrounding orifice was established. The arc duration did, therefore, not exceed one cycle. Bulky design, large moving masses, and considerable air consumption were the price to be paid for this performance. A two-stage alternative with two nozzles of smaller diameter appeared to offer little, if any, gain in economy, simplicity, or air consumption. Subsequently an insulating nozzle was adopted utilizing the discovery that, while none of the inorganic insulating materials available at the time would withstand arc temperatures, certain organic materials, rubber in particular, are self-protecting due to very thin surface layers becoming volatilized when in contact with the arc. The moving contact

fills the throat of the insulating nozzle, thereby blocking the air flow until the contact tip is withdrawn past the narrowest cross section. Several half cycles pass in this way during which the arc is drawn in still air. The arc energy remains negligibly small, and oscillographic records hardly reveal any trace of arc voltage. As soon as the orifice is free and the air flow is established, the whole length of the already existing arc column is exposed to its scavenging action, with the arc root being positioned so as to be subject to the most effective combination of pressure and velocity. It was, of course, realized that this solution was a compromise, putting simplicity, ease of mechanical operation, and air economy before minimum over-all operating time. It should not be overlooked that in Central Europe, with practically all transmission systems resonance grounded, there is virtually no stability problem and no demand for utmost reduction of the fault duration. True, the interruption of line charging currents at the first current zero, an important asset of the metal nozzle type, had also been sacrificed. A number of interruptions and restrikes would inevitably precede the final extinction of the charging current, but no cumulative overvoltages were anticipated in view of the low dielectric strength of the air gap during this period. In 1935 the writer had an opportunity of carrying out a series or such tests with the 220-kv air-blast circuit-breaker in Chevilly after it had been modified into a single-break candle type with insulating nozzle. Only one line was connected to the 220-kv bus bars. The arc duration did not exceed six half cycles (referred to 50 cycles), and was not disappointingly long if compared with the performance of an oil circuit-breaker of the dead-tank type with explosion pots, requiring 13 half cycles.

The Chevilly type, 30 feet high, was succeeded by a double break design for 220 kv, only 18 feet high, with internal capacitive control of the voltage distribution, Figure 1 of the discussion. Reduction of the stroke was obtained by shielding the contacts, and in particular withdrawing the movable contact behind a spherical metal screen. As in this way the internal insulation had become superior to the external insulation of the shortened interrupting chamber, the latter was supplemented by a series isolator. To understand the subsequent technical development it is necessary to appreciate the then prevailing situation. The power supply companies, while most apprehensive of the use of great quantities of oil in extra-high-tension circuit-breakers, were not prepared to pay higher prices for an oilless design. Competition with small-oil-volume circuit breakers was also intense, amounting to a feud between manufacturers of different persuasion. A further target was set by the demand for circuit-breaker types up to 220 kv transportable on railway trucks in completely assembled state ready for immediate service.

These were the conditions from which the free-jet circuit breaker originated, partly an austerity type and partly an inspired venture; it took its name from the German denotation for impulse water turbines. The design has been admirably described by the authors. Within the scope of this discussion the numerous details of pneumatic equipment, which were the accumulated results

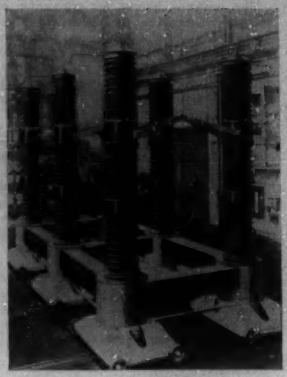

Figure 1

of ten years development and operating experience, must be passed over, foremost among them the design of the blast valves and the choice of material for the valve seating. In electrical respect the arrangement under consideration differs from that known to the writer (design stage 1937) inasmuch as it employs hollow contacts and abandons the use of weather exposed cup-shaped ebonite heads moulded around a metal core consisting of spherical stress control shields. The following comments refer to points raised by the authors.

The interrupting device is more accurately described as a single-stage double blast arrangement. It is designed to interrupt cross-country faults in systems with ungrounded or resonance grounded neutral in which case one pole is subject to a recovery voltage of 220 kv.

While tungsten tips were found useful with contacts of the male and female type, ordinary copper would appear to be more suitable for butt contacts. The problem of erosion should not arise for the moderate short-circuit currents of extra high voltage systems. To the writer's recollection after a few short circuit operations the voltage drop of special arc resisting alloys ceased to meet the requirements of current carrying contacts.

The authors have given a fair account of the critical performance of the circuit-breaker when interrupting line charging currents. It should be worth while to further investigate the causes of the prolonged arcing after the retractable contacts have been fully withdrawn. In the writer's own experience with the first circuit-breakers of this type the period during which the contact clearance acts as a safety gap was always followed by speedy final extinction. As regards ultra-short interrupting times, there is no inherent limitation in the circuit-breaker design, and it has been explained before how, due to a different approach to the stability problem, this target did not arise.

To carry out tests with the three poles connected so as to form a multistage single-phase circuit breaker should not be expected to supply useful information unless the voltage distribution is controlled by considerable external means. In their final effect such means are equivalent to simply testing one stage at reduced voltage, whereas in fact the stroke should be reduced simultaneously.

Some may reject the free-jet circuit breaker as a "depression type," or condemn it by faint praise, others will hail it as a timely and most promising departure from a conventional technique. As in this unique case the inventor has no possibility to stand up for the development he has initiated, the writer feels it his duty to state that the credit goes to J. Biermanns, the man who has left his mark in every field of European high tension engineering, has revolutionized German transformer engineering, and has between 1926 and 1945 built up our present day air-blast circuit-breaker technique. He can be congratulated that against all previous opposition at home and abroad he could now see his principal ideas and basic designs becoming the foundations of a new art. In his latest creation, the free-jet circuit breaker, there are enough convincing features to look forward with interest to an improved balance of electrical characteristics.

Eric T. B. Gross (Illinois Institute of Technology, Chicago, Ill.): In comparing the performances of this air-blast circuit breaker of German origin and of oil circuit breakers for the same voltage which were built in this country, one should keep in mind that the German circuit breaker was built eight years ago (as the nameplate data indicate), which means that its construction is based on designs made in or more likely before 1939. Realizing that designs in the United States have been considerably improved during the past ten years, one must agree with the authors' conclusions that the many apparent advantages of such a type of air-blast circuit breaker deserve widespread interest. Anybody who was either present at the tests, or had the opportunity to see the movie shown in Pittsburgh after the presentation of the paper will be highly impressed with the performance of so simple a circuit breaker construction.

I understand that this particular type of circuit breaker is based on Dr. J. Biermanns' inventions, and that the first circuit breaker of the free-jet type for 110 kv was built in about 1935. This type of circuit breaker is, therefore, a more recent design and it seems possible that further development, based on field-experience, will bring it up to present-day standards. It is very likely that some research was done after 1939 in the AEG switchgear works, and its circuit breaker and high voltage test and research laboratory, the results of which could be used also in further studies and development work.

Whereas this free-jet type air-blast circuit breaker is of comparatively recent design, the earlier AEG-type air-blast circuit breakers which were based on patents of the late Professor S. Ruppel were first put into practical use in about 1928 (long before Hitler's days), and had reached standardization by 1932. This fact is of interest because it indicates clearly that technical considerations and not political pressure brought about the elimination of

oil from circuit breakers for all voltages. This German development was followed by the large manufacturers in most European countries, and we learn that one of the large manufacturers in Switzerland has already discontinued to produce any type of oil circuit breakers.

The paper leaves the impression that representatives of the German manufacturer were not present to help with the assembly and the test of the circuit breaker; I am wondering if this assumption is correct, and if so, whether the authors feel that the tests showed the best performance which this circuit breaker is able to give. It should be of interest too to know where the circuit breaker was originally installed in Germany on the 220-kv system, and what duties it had to perform at that location (actual short circuit megavolt-amperes, interruption of charging current of how many miles of equivalent 60-cycle line, and so forth).

The authors deserve an expression of gratitude from the profession for their interest in this part of equipment; such tests provide invaluable information not otherwise available, and they indicate in which direction improvement of our present-day equipment can be expected.

Alexander Dowjikov and Clifford C. Diemond: We welcome very much the "missing link" of our presentation, namely, the history of the development of the tested circuit breaker, kindly contributed by Gross and Willheim. All during the tests we were sorry that the originators of this type of circuit breaker were not able to attend the tests. We are glad that one of the originators (Willheim) recognizes our "very fair and tactful approach and the most efficient way in which we discharged our delicate and difficult task" of testing the circuit breaker without competent representatives of the manufacturer. Unfortunately, we cannot even tell Gross when the circuit breaker was installed in Germany and what duties it had to perform at that location; we had to base our installation and tests on the basis of the nameplate data, on the instruction book for this type of circuit breaker which we were fortunate enough to obtain from our friends in Sweden, and on our own careful study and interpretation of functions of different parts of the circuit breaker. We believe that we obtained from this circuit breaker a quality of performance essentially equal to its maximum capabilities. We hope that the described tests witnessed by representatives of all circuit breaker manufacturers and many operating companies will bring to focus further development of this type of circuit breaker which has many attractive features even if in its present form it does not measure up to the performance of the best circuit breakers of American manufacture.

New Devices Derived From a Torque Balance Telemeter

W. H. BURNHAM
ASSOCIATE AIEE

Synopsis: The continued increase in remote indication of electrical and nonelectrical quantities in this country and abroad has taxed the ingenuity of the engineer to provide new systems and auxiliary devices to meet their needs. One of the better known devices used for remote indication of electrical and nonelectrical quantities is the torque balance telemeter. The successful use of this telemeter has suggested the development of two pieces of auxiliary telemetering equipment which greatly broadens the field of application for this telemeter as well as serving to augment the general field of telemetering. These new devices, known as the load injector and the converter, working in conjunction with the torque balance telemeter provide the additional functions of signaling, and the totalizing and integrating of a telemetered quantity.

Torque Balance Telemeter

SINCE THESE NEW devices are a modification of the standard torque balance telemeter, a brief review of this telemeter is a desirable approach to the explanation of these two devices.

The torque balance telemeter[1] is capable of telemetering several quantities such as power, voltage, current, or angular position. Each of these quantities is telemetered by converting the quantity to a proportional value of d-c milliamperes which is transmitted over wire lines to an indicating or recording milliammeter whose scale is marked in units of the measured quantity.

The torque balance telemeter is a type of servomechanism in which two opposing torques are used to achieve the proportionality between the measured quantity and the output current in milliamperes. Basically, the device contains an operating mechanism, a restraining mechanism, a photoelectric follow-up system, and a power supply. The operating mechanism will depend upon the quantity being telemetered. Figure 1 shows a functional analysis diagram[2] of the device for telemetering power. In this case, the operating mechanism, which is shown as the primary detector, is a wattmeter mechanism without the control spring. The restraining mechanism is a d-c milliammeter mechanism with very low torque lead-in spirals substituted for the control springs. Both mechanisms are connected to a common shaft which also supports a small mirror. A beam of light is reflected from the mirror onto two phototubes. The relative amount of light on the phototubes determines the polarity and magnitude of the d-c output from the power supply.

With no input to the operating mechanism, the beam of light is divided equally between the two phototubes and the d-c output from the power supply is zero. When the operating mechanism is energized, the common shaft turns and the light reflected from the mirror illuminates one phototube more than the other, which causes the power supply to produce an output current. Since the d-c output flows through the restraining mechanism, it will produce a torque on the common shaft which, by virtue of proper lead connection, will be opposite to that produced by the operating mechanism. The output current will increase until these two torques are balanced. For this condition, there will be a definite distribution of light on the phototubes which means a certain value of output current for a given value of watts input to the operating mechanism will be established. Figure 2 shows the mechanical arrangement of the instrument mechanisms.

Normal supply voltage variations and changes in effective output resistance do not appreciably affect the accuracy of the device, since the photoelectric follow-up system immediately corrects for changes in output due to these variations. For convenience of installation, testing, and flexibility, the torque balance telemeter is now always built in a draw-out case for either surface or semiflush mounting. Figure 3 is a front view of the telemeter mounted in the draw-out case.

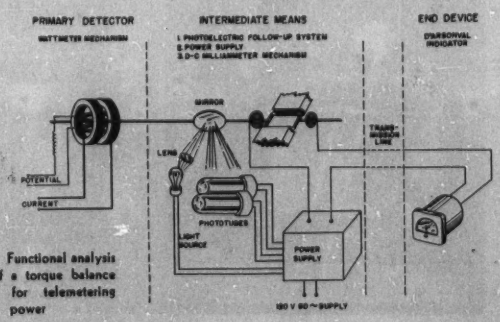

Figure 1. Functional analysis diagram of a torque balance telemeter for telemetering power

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W. H. BURNHAM is with the General Electric Company, West Lynn, Mass.

Torque Balance Load Injector

With the many telemeter channels associated with a power system, channel outages are sure to occur occasionally. When this happens it is very desirable to have the indicating, totalizing, and recording circuits function normally. It is also desirable to have a signaling system between the dispatcher's office and power stations to tell the individual generating stations what load they should be carrying. The effectiveness of this system is increased if alarm features are incorporated to call the station operator's attention to a change in required load.

These requirements are fulfilled by the load injector, which is essentially a manually adjustable d-c milliampere power supply with special features incorporated. The output of the power supply can be varied from 2 to 50-milliamperes and will remain substantially constant when once set, even with normal supply voltage variations or changes in the load resistance. This device can also be made having lower output ratings such as 1 to 25-milliamperes. Figure 4 shows a diagram of the device. Two major changes were made in the torque balance telemeter to produce this device. First, the power supply was redesigned to provide the increased output current; and secondly, the operating mechanism was replaced by two instrument control springs. By means of a gear linkage the control springs can be rotated by a knob on the front panel. The torque produced on the shaft by the springs produces the same results as any electrical operating mechanism and becomes the input. Except for this manual adjustment of the input, the operation is nearly identical with that of the torque balance telemeter.

Variations of plus or minus ten per cent in the supply voltage result in only a plus or minus one per cent change in output current. The maximum value of load resistance into which it will operate with full ouptut is 1,000-ohms, and the variation in output current throughout the range of 0 to 1000-ohms load resistance is less than two per cent of full scale.

Figure 5 shows the front panel arrangement of the control knob, power switch, and a reversing switch for selecting the polarity of the output current. It is intended to be used with an indicating milliammeter, as a guide to the operator in adjusting the output, so the output control is not calibrated. The device is mounted in a semiflush case with draw-out construction.

When a channel outage occurs, it is usually possible to telephone the generating station and obtain the load that should be telemetered over the faulty channel. Then the load injector is connected to the receiving end of the channel and its output adjusted to this value of load. This enables the totalizing and recording circuits to function properly.

As a signaling device, the load injector may be located in the dispatcher's office and the receiving instrument in a generating station. With the instrument scale marked in units of load, the dispatcher can indicate to the station operator what load he should be holding.

Torque Balance Converter

As previously mentioned, the output current of the torque balance telemeter representing the measured quantity can be indicated and recorded. However, when telemetering power, it is often desirable to know the amount of energy generated during a certain period. This information can be obtained only by using a planimeter on the recorder chart, or a similar method to multiply the average value of power by the time. A similar problem exists in obtaining an integrated reading of power flow over tie lines. Since tie-line power flow may be in either direction, it is necessary to integrate the area under the curve on each side of zero on

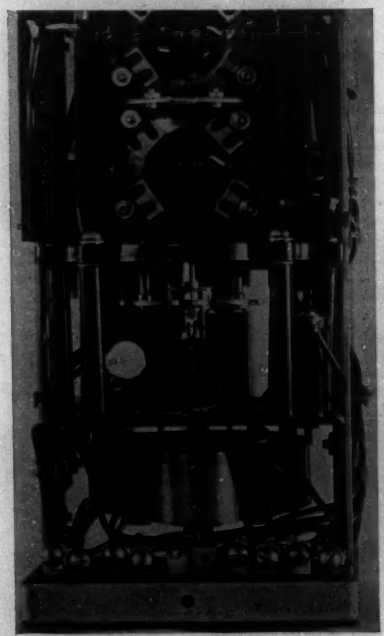

Figure 2. Torque balance telemeter for telemetering three-phase power showing the mechanical arrangement of the instrument mechanisms

Figure 3. Torque balance telemeter mounted in the drawout case

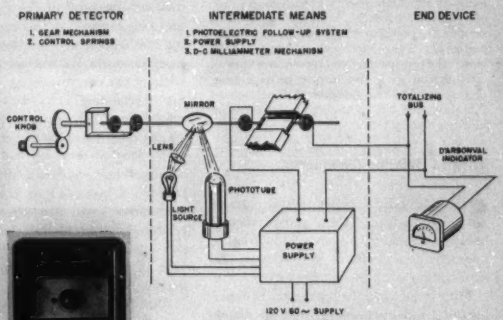

Figure 4 (above). Functional analysis diagram of the torque balance load injector

the recorder chart and multiply by the time to obtain the resultant value and direction of the energy flow. When telemetering power, a very common requirement is that a reading be transmitted that represents the total of several power sources. If the telemeter transmitter operates on the watthour meter principle, some means of producing watts in proportion to the totalized reading must be used to operate the transmitter.

To solve these problems and provide directly a continuously integrated reading of the quantity from the output of a torque balance telemeter, the torque balance converter has been developed. If a quantity can be expressed in terms of watts, it can be integrated with respect to time by applying the watts to a watthour meter. The converter performs the conversion of d-c milliamperes to single phase a-c watts. By applying these to a watthour meter, a continuously integrated reading of the original quantity is obtained. The converter, shown in Figure 6, consists of two components, the converter unit and an amplifier. The converter unit, which is mounted in a two-element watthour meter case with draw-out construction, consists of a polyphase wattmeter mechanism and a d-c milliammeter mechanism connected to a common shaft. The control springs are replaced with very low torque lead-in spirals. The amplifier is conventional except that the output stage

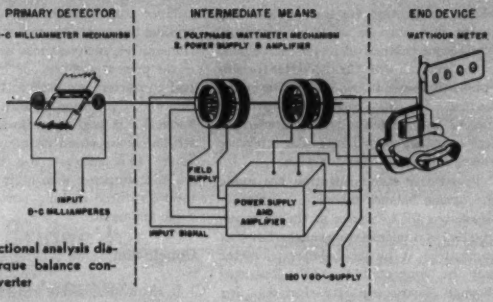

Figure 7. Functional analysis diagram of the torque balance converter

is designed to deliver power at a high current level, so it can be considered a current amplifier.

The general principle of operation is similar to the torque balance telemeter and load injector in that it is a servomechanism in which balanced torques determine the output for a given input. A diagram of the converter is shown in Figure 7. One of the wattmeter mechanisms is used as a variable ratio transformer. That is, the current coil is excited with its rated current and a voltage is taken off the potential coil. The magnitude and phase of this voltage depends upon the relative position of the potential coil with respect to the current coil. The other wattmeter mechanism is used as a watt restraining mechanism, and the milliammeter is the operating mechanism. The voltage induced in the secondary coil of the variable-ratio transformer is the input signal to the amplifier. The load on the output of the amplifier consists of the current coil of the watt restraining mechanism and the current coil of a single-phase watthour meter in series. Their potential coils are excited from the 120 volt a-c supply. The operation is as follows: With zero input to the converter, the variable ratio transformer has zero coupling and no signal is applied to the amplifier. Thus the output current is zero and no power is applied to the watthour meter. As in the torque balance telemeter, energizing the operating mechanism rotates the common shaft. This results in a signal being applied to the amplifier. The output current does two things. It produces rotation of the watthour meter disk, and causes a torque to be produced by the watt restraining mechanism in the converter unit. This torque will be opposite to that produced by the operating mechanism, by virtue of the proper connections to it. Therefore, the shaft will rotate until sufficient output current is flowing to establish a balance of these two opposing torques. When the balanced condition is obtained, a definite amount of power will be represented by the output current and the 120 volts supplied to the potential coils. If the input milliamperes to the converter is doubled, the output power will be doubled as the result of a new balance being established. The watt restraining mechanism may be considered a monitor of the watts output since it determines the magnitude of output current for a given value of milliamperes input to the converter.

The error that is introduced by the nonlinearity of the watt-restraining mechanism is held to a small value by limiting the angle through which the shaft turns to only a few degrees. Normal line voltage variations have little effect upon the conversion accuracy since the watt-restraining mechanism will adjust the system by changing the output current.

If the polarity of the input d-c milliamperes is reversed, the power flow to the watt restraining mechanism and the watthour meter will reverse. This reversing feature makes the converter very adapta-

Figure 5. Torque balance load injector showing the control knob, power switch, and reversing switch

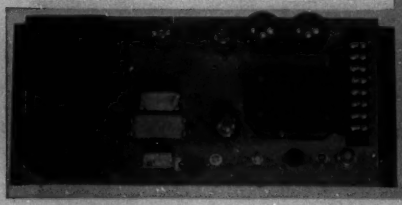

Figure 6. Torque balance converter, consisting of the converter unit and an amplifier

ble to integrating tie line loads where power flow may be in either direction. The torque balance telemeter can be used in conjunction with the converter for this application since it also has this reversing feature. In this case two watthour meters with detents are connected in series so that each meter will integrate power flow in only one direction.

Progressive standardization has made the torque balance telemeter an attractive device for totalizing at the transmitting end of a telemeter channel. For this application, a torque balance telemeter may be connected to each generator through instrument transformers, for measuring watts or vars, and the outputs paralleled. The totalized d-c milliamperes can then be applied to the torque balance converter and the output watts applied to a watthour meter impulse type transmitter, such as those used in several of the present systems. Even when thermal converters are used at the transmitting end, the torque balance converter is applicable if used in conjunction with a self-balancing potentiometer, which is a device that converts d-c millivolts to d-c milliamperes with high speed of response without moving parts other than a galvanometer.

Conclusion

In this paper, two new auxiliary devices for use with the torque balance telemeter and other types of telemeters have been presented. It is expected that their use will help fill the needs in the expanding field of telemetering. The load injector, in providing a means of signaling and acting as a phantom transmitter should raise the efficiency of a telemetering system. At present, it is expected that the torque balance converter will find application as an integrating device and a means of transmitting a totalized reading, but this, by no means, exhausts the possibilities of this device in telemetering and associated fields.

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No Discussion

A Direct Reading D-C Bridge for Microwave Power Measurements

HERBERT J. CARLIN
ASSOCIATE AIEE

JUDD BLASS
NONMEMBER AIEE

Application of Bolometers at Microwave Frequencies

BOLOMETRIC METHODS for the determination of absolute power level have become established measurement procedures[1] for frequencies in the microwave region. By proper use of a bolometer, power from a microwave source may be directly compared with known power magnitudes from a d-c or audio-frequency source. The developed techniques utilize the temperature-resistance characteristics of the bolometer. The change in resistance of a bolometer element due to a change in power is independent of the type of excitation. Thus an unknown amount of radio-frequency power which results in a given change of resistance may be equated to a known quantity of audio frequency or d-c power which results in an identical change in resistance. The basic problem, then, is the design of low frequency circuits and instruments to accurately and conveniently measure small increments of resistance.

There are two general methods available for measuring this resistance change; both employ a Wheatstone bridge in which one arm is a bolometer. In one method the bridge is always maintained at balance; in the other it operates unbalanced. The basic bridge schematic is illustrated in Figure 1. With radio-frequency power absent, the bolometer resistance is varied by adjusting the power of the biasing source until the bridge is balanced. The bolometer then is exposed to the unknown power supplied by the microwave source. This additional power causes a temperature rise which results in a change in the bolometer resistance with a subsequent unbalance of the bridge. When the unbalanced bridge scheme is used, the off-balance reading of the detector may be employed as the measure of the radio-frequency power, provided the indication of the null meter is calibrated against a known low frequency (or d-c) power increment. In another embodiment of this method, the change in bolometer resistance is measured by using variable calibrated resistors in the other branches of the bridge.

If the measuring system is to be operated as a balanced bridge, the incremental power source is adjusted for a null indication on the detector, and the increment of audio frequency (or d-c) power required to rebalance the bridge is a direct measure of the microwave power absorbed in the bolometer. Although the unbalanced bridge method may be a more straightforward procedure, in some respects it is inferior to the balanced bridge method. In a practical unbalanced bridge circuit a simple procedure must be devised for calibration; and the zero reset of the bolometer must be checked. Aside from these problems, a fundamental difficulty is that the bolometer resistance does not remain constant during the measurement process. This requires that the unbalanced detector be calibrated over its entire range of operation, and completely recalibrated when the bolometer ages or a new bolometer is used. In addition, the bolometer is used to terminate a microwave transmission line, and in broad-band power meters[2] the bolometer resistance is the most important component of the line termination. If this resistance is permitted to vary, the broad-band match may be affected adversely, and large errors due to reflection loss introduced.

The remainder of this paper is devoted to a discussion of balanced bridge methods in microwave bolometer power measurement which obviate some of these difficulties.

Balanced Bridge Methods

The bias supply for most bolometer bridge circuits is direct current. Such a source must have low inherent drift, for any change in bias level after the initial balance is introduced as an error in the measurement of the radio-frequency signal. The bias excitation may be provided by a rectifier power supply, but the requirements for low drift and high stability, and the need for relatively large bias currents, as in the case of high power bolometers (of the order of 100 milliamperes), make the design of such a supply somewhat of a problem. Furthermore, an instrument designed for field use will encounter a wide variety of main power sources, with poor regulation the rule rather than the exception. This array of difficulties suggests the use of an internal battery-operated bias supply. The bridge described in detail in this paper does in fact make use of a small storage battery supply with an output of 24 volts and weight of less than ten pounds. The ampere-hour capacity is large enough to permit normal operation of the bridge for about a month before recharging is necessary.

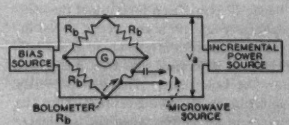

Figure 1. Block diagram of the bridge method for measuring microwave power

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HERBERT J. CARLIN and JUDD BLASS are with the Microwave Research Institute, Polytechnic Institute of Brooklyn, Brooklyn, N. Y.

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As stated earlier, the incremental power source may be either audio frequency or direct current. In the former case the frequency must be chosen high enough to insure a period of oscillation which is fairly small compared with the thermal time constant of the bolometer. The audio oscillator must have high amplitude stability, and sufficient output to replace the maximum level of radio-frequency power absorbed by the bolometer. Output level may be measured with a vacuum tube voltmeter whose stability requirements are similar to those of the oscillator. At bridge balance the audio power into the bolometer of the bridge in Figure 1 is $v_a^2/4R_b$, where v_a is the output voltage of the oscillator and R_b is the resistance of the bolometer. If the vacuum-tube-voltmeter is to give a direct indication of the incremental power, the bridge arms must be compensated to give a true impedance balance. If an a-c high gain detector is used for a null indicator, the circuit must be carefully shielded and grounded to hold extraneous pickup to an absolute minimum. This is especially true if low radio-frequency powers of the order of 20 microwatts or less are to be measured with any degree of accuracy.

If a d-c incremental power source is used, it need not be physically separated from the bias supply but can be incorporated directly into the bias circuit. This consideration, with the elimination of the difficulties encountered with audio frequency when used in bolometer circuits, makes the all d-c bridge a very satisfactory instrument. Another advantage of the d-c bridge for field use is that no electronic tubes are needed.

The major advantage of an audio frequency substitution method is the orthogonality between a-c and d-c power in a resistor. That is, the total power is proportional to the sum of the square of the a-c current and the square of the direct current only. No cross product terms combining the a-c and d-c quantities are present. Thus the incremental power due to a change in audio current is entirely independent of the d-c bias level in the bolometer, and may be measured directly in the audio frequency circuit alone. This is not true, however, if the incremental power is direct current. In this case, the change in power is a function of the original bias level, and a knowledge of the incremental direct current alone is not sufficient to determine the true d-c power into the bolometer. This can be easily seen from the following expression for the change in d-c power, when the bolometer bias current I_b is changed by an amount ΔI.

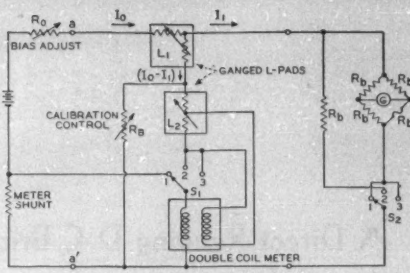

Figure 2. Basic summation-type direct-reading wattmeter circuit

Change in d-c power

$$= P_{rf} = [I_b^2R_b - (I_b - \Delta I)^2R_b]$$
$$= [2I_b\,\Delta I - (\Delta I)^2]R_b$$

The bridge described operates as a balanced circuit, and is an all d-c instrument with self-contained battery pack. It therefore possesses the advantages associated with the elimination of an audio frequency oscillator and rectifier power supply. The presence of cross product terms in the expression for incremental power is taken into account by a specially devised circuit arrangement which permits the measurement of replacement power by a direct reading d-c instrument.

Bridge Operation

The total power dissipated in the bolometer at balance is fixed for a given bolometer. If the bridge is balanced by d-c power alone, and radio-frequency power is then introduced into the bolometer, the radio-frequency power is equal to the amount by which the d-c power must be reduced in order to effect a rebalance. Thus if the d-c bolometer current required for balance when radio-frequency power is absent is $I_0/2$, referred to as the bias current, and $I_1/2$ is the d-c bolometer current present at the rebalance after the addition of microwave power, the microwave power (P_{rf}) is

$$P_{rf} = \frac{R_b}{4}(I_0^2 - I_1^2) \tag{1}$$

This expression may be evaluated by determining I_0 and I_1 and subtracting their squares. A method like this suffers, however, from extreme inaccuracy when the power to be measured is small compared to the bias power, for then I_1 differs only slightly from I_0. Equation (1) may be rewritten as

$$P_{rf} = \frac{R_b}{4} I_0\left(1 + \frac{I_1}{I_0}\right)(I_0 - I_1) \tag{2}$$

Let the d-c power to the bridge be attenuated by the 4-terminal constant-input resistance network shown in Figure 2, so that the battery loading is constant, independent of attenuation. The difference $(I_0 - I_1)$, in equation 2 is the current flowing in the shunt branch of the attenuator. The sum factor, $[1 + (I_1/I_0)]$, is a parameter of the attenuator alone. Let

$$\frac{I_1}{I_0} = k$$

and

$$I_0 - I_1 = I_2$$

Equation 2 becomes

$$P_{rf} = \frac{R_bI_0}{4}(1+k)I_2 \tag{3}$$

The line current, I_0, is fixed for a given bolometer. R_b is the resistance of the bridge at balance. The microwave power is therefore linearly proportional to $(1 + k)I_2$.

In order to determine kI_2, a network consisting of an attenuator whose termination is a low resistance ammeter, is inserted in the shunt branch of the load attenuator. This network is ganged to the load attenuator, so that the k factors of both are equal for any attenuation setting of the network. The current I_2 can be measured by another ammeter in the shunt arm of the load attenuator. Rather than use two ammeters to obtain $I_2 + k\,I_2$, a double-coil instrument is employed which sums the currents automatically.

To make this instrument direct reading it is necessary to introduce the proportionality factor $R_bI_0/4$. This constant varies with individual bolometers due to manufacturing variations which preclude the same bias for each unit. The factor is essentially a meter scale constant and is calibrated for each bolometer by means of a variable shunt, R_B (Figure 2), across the double-coil ammeter. If the face calibration of the meter is W microwatts per milliampere and P_B is the ratio of the current in the meter I_m to the branch

current I_2 and if the meter is to read the power, the following must be true:

$$P_B W = R_b I_2/4 \tag{4}$$

The meter attenuator must be a constant impedance network in order that P_B be constant for all attenuator settings. The procedure used to calibrate the meter conveniently is described below.

Refer to Figure 2. L_1 and L_1' are the ganged load and meter attenuators. The input impedance at terminals $(a\text{-}a')$ is equal to R_b when the bridge is balanced. R_B is the calibrating shunt used to obtain the proper scale factor for the summation meter. A complete calibration and measurement procedure is as follows: with the attenuators out, the bridge is balanced by means of the "bias adjust" control R_0. The switches S_1 and S_2 are turned to position 1 and the current I_0 is measured with the differential ammeter now operating as a single coil instrument inserted in the line. The switches then are turned to position 2, and L pads are inserted whose attenuating factor k is determined by the equation

$$\frac{R_b}{4}(1-k_0^2)=1 \tag{5}$$

Since equation 3 may be rewritten

$$P=\frac{R_b I_0^2}{4}(1-k^2) \tag{6}$$

the setting, $k=k_0$, of equation 5, corresponds to the value which would be necessary to balance out a microwave power of I_0^2 microwatts. I_0^2 is known from the meter reading at the initial bridge balance. The calibration is accomplished by varying R_B, with $k=k_0$, until the summation meter indicates I_0^2 numerically in microwatts. The meter is now calibrated correctly over its complete range. The switches are set to position 3 and radio-frequency power may be introduced. The unbalance is now corrected by means of the L pad controls. At balance the meter reading directly indicates the absolute microwave power.

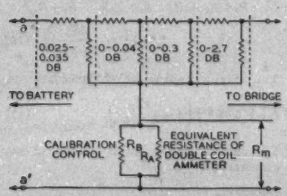

Figure 3. Equivalent bridge attenuator circuit including the metering resistance of R_B and R_M in parallel

The wattmeter model that has been built is designed to use a Wollaston wire element for low power measurement (0–1 milliwatt), and a metallized-glass element for high power measurement (10–100 milliwatts). The low power level is divided into two ranges, 0–100 microwatts and 0–1 milliwatts. The bias current is between 3 and 4 milliamperes. The calibration power, I_0^2 is about 50 microwatts. The calibration for this power level is made on the lowest range (0–100 microwatts) and holds true for the higher one (0–1 milliwatt). In the first range, 0–100 microwatts, I_2 never exceeds 100 microamperes. The range of the double-coil instrument is 200 microamperes and operates satisfactorily as an unshunted instrument for this part of the lower range. In the (0–1 milliwatt) range, I_2 may be as great as 1 milliampere, and the instrument must have a ten to one shunt across it in order that full scale deflection corresponds to 1 milliwatt. The design must take into account the fact that the load of the meter attenuator should remain constant for the calibration to be true for both the 0–100 - microwatt and 0–1 - milliwatt ranges. In the case of the metallized-glass bolometer the bias current is between 30 and 40 milliamperes, and I_0^2 is approximately 5 milliwatts. In order that the calibration point be at a reasonable scale position in the high power range, k_0 is chosen so that

$$\frac{R_B}{4}(1-k_0^2)=10$$

Bridge Design

A range of attenuation from 0.03 decibel to 3.0 decibels is needed to cover the limits of radio-frequency power to be measured. In order that accurate tracking be maintained between the load and meter attenuators, and that the two input impedances remain very nearly constant for all settings, it was advisable to design the attenuators as cascaded units consisting of resistors mounted on multiple point switches. The attenuations of the units are 0–2.7 decibels in 0.3-decibel steps, 0–0.3 decibel in 0.05 steps, 0–0.04 decibel in 0.01 steps, and 0.025–0.035 decibel in 0.001-decibel steps. The lowest steps of attenuation result in at most 1-microwatt steps for the low power bolometers and 1-milliwatt steps for the high power bolometers.

It is very important that the deviation of the input impedance from R_b be kept as small as possible. If I_0 is the line current at both balance and rebalance, and I_1 is the bridge current at rebalance, the power

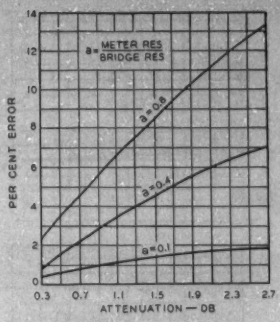

Figure 4. Effect of meter resistance on wattmeter error as a function of attenuation for the uncompensated attenuator

$$a=\frac{\text{meter resistance}}{\text{bridge resistance}}$$

in the load is proportional to $(1+k)(I_0-I_1)$. If, however, the line current at rebalance has changed to $I_0+\Delta I_0$, I_2 then is equal to $I_0-I_1+\Delta I_0$. I_1 remains the same, as it must for the same microwave power. In the latter case the measured power is proportional to $(1+k)(I_0-I_1+\Delta I_0)$. Although ΔI_0 is small compared to I_0 it introduces a substantial error when considered as a part of the difference current I_2. A one per cent change in the line current very easily may introduce a ten per cent error in the power measurement. The error in the power reading as a function of the deviation of the input impedance of the load attenuator, where it is assumed that the meter attenuator is correct both in attenuation and input impedance, is given by

$$\text{Percentage error}=\left[\frac{k'-k}{1+k}+\frac{\delta R_b}{R_0+R_L}\left(1+\frac{2k'^2}{1-k'}\right)\left(\frac{1}{1+k}\right)\right]\times 100 \tag{7}$$

The derivation of this equation is given in the appendix. δR_b is the deviation of the input impedance from R_b, k is the shunting factor of the meter L pads for the given setting, and k' is the shunting factor of the complete network between battery and bridge. k' should equal k, but the L-pad resistor tolerances and other causes of errors in input resistance, at the same time give rise to tracking errors between the meter and load attenuators, so that the respective k factors of these units are no longer identical.

It may be pointed out that in addition to input resistance variation, another source of line current change is battery drift. The effects of this drift may be

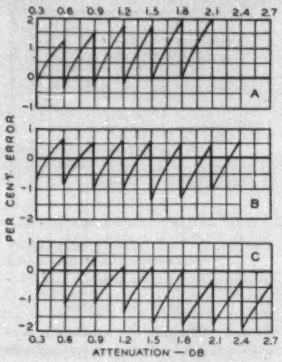

Figure 5. Effect of meter resistance on wattmeter error when attenuator is compensated

A. Maximum R_B, for high bias level bolometer; meter resistance ÷ bridge resistance = 0.5

B. Average R_B, for medium bias level bolometer; meter resistance ÷ bridge resistance = 0.4

C. Minimum R_B, for low bias level bolometer; meter resistance ÷ bridge resistance = 0.35

eliminated, however, by occasionally repeating the bias level adjustment.

The meter network is a cascade of L-pads, the design of which is straightforward.[3] It is seen from Figure 3 that the bridge attenuating network, however, cannot be looked upon simply as a cascade of L-pads because of the presence of the resistance of the metering circuit. If the total impedance of the meter is as small as ten per cent of the load impedance it must be considered as part of the bridge attenuating network. Under this circumstance it is impossible to obtain a constant impedance input for all settings of the attenuator. If the ratio of meter to bridge resistance is sufficiently low, it is possible to compensate the circuit so as to keep the input impedance constant within a small tolerance. This may be seen from the following. The input impedance to the uncompensated network, when all pads are out but the one covering the range from 0–2.7 decibels, is greater than R_b. If the 0–0.3-decibel pad is cascaded with the 0–2.7-decibel pad the resulting impedance is even larger. In the compensating process, the 0–2.7-decibel pad is designed so that when operating as the only attenuator in the circuit the input impedance is less than R_b, and when cascaded with a similarly compensated 0–0.3-decibel attenuator the resulting input impedance is slightly greater than R_b. The maximum deviation is thus reduced. It is unnecessary to compensate the 0–0.04-decibel and 0.025–0.035-decibel attenuator since the value of resistance in their shunt branches is very much greater than the meter resistance. A graphical plot of wattmeter error, as a function of attenuation and the ratio of metering circuit resistance to bridge resistance, comparing the compensated and uncompensated designs, is presented in Figure 4 and Figure 5. The error is calculated from equation (1)–(7). δR_b, the deviation of the input resistance from R_b, is due to the presence of the metering resistance R_m (Figure 3), and involves the analysis of the complete attenuating network. The resistance of the meter circuit depends upon the setting of R_B, the bolometer calibration control. R_m is thus a function of the bias level ($I_0/2$), so that the error is a function of the bias level as well. It is seen that in general the error of the compensated design is considerably less than 1.0 per cent, although at a few isolated points, representing extreme operating conditions, it may rise to 2.0 per cent.

Broad-band Bridge Operation

Microwave attenuators[4] have been designed to be used with the low power and high power bolometers so that power measurement may cover the entire range from 0–5 watts over a broad band of frequencies. The use of these attenuators introduces certain obstacles in direct power measurement. Primarily, the attenuation is not exactly constant over the band. Secondly, the tolerances in the manufacture of the attenuators do not permit of integral power ratios even at midband. Rather than complicate the use of the wattmeter with unnecessary calculations on the part of an operator, a circuit has been devised which enables one to measure the microwave line power as a simple multiple of the wattmeter reading. A variable control is placed across the input to the attenuation network at the points (a-a') Figure 2. When the bridge is initially balanced, the line current differs from the bridge current by the amount shunted off by this control. Since the meter is calibrated at I_0^2 (where I_0 is the battery current), there is an error introduced into the calibration. This error is of the correct magnitude to account for the tolerance of the attenuator.

If the nominal attenuation of the attenuator is α_N decibels, and the calibrated value (a figure usually supplied by the microwave attenuator manufacturer) is α_C decibels, the attenuator tolerance compensator is designed to operate correctly when the value

$$F=\frac{\text{antilog } \alpha_C/10}{\text{antilog } \alpha_N/10} \tag{8}$$

is set on the control. This may be used conveniently to compensate for the attenuation swing of a radio-frequency attenuator if the calibration curve of the latter is known.

Meter Sensitivity

The deflection sensitivity needed for the galvanometer, used as a null indicator, is a function of the bolometer sensitivity, and the minimum change in power the bridge is required to detect. The expression for the unbalance current in a Wheatstone bridge which is almost balanced[5] is

$$i=\frac{(\Delta R_b)I_0}{4(R_b+R_{gal})}$$

where ΔR_b is the deviation of the resistance of the bolometer from its value at bridge balance. For a 200-ohm bolometer with 6,000 ohms per watt sensitivity, and a bias current of 4 milliamperes, a 50-ohm galvanometer requires a sensitivity of 0.5 microamperes per division to detect an unbalance of 2 microwatts. If further wattmeter sensitivity is required a more sensitive galvanometer may be employed. If the above galvanometer is used with a high power bolometer of 60 ohms per watt sensitivity and a bias current of 40 milliamperes, an unbalance of 2 milliwatts may be detected.

Appendix

From Figure 2, the zero adjust line current is

$$I_0=\frac{V}{R_0+R_b} \tag{9}$$

at radio-frequency balance the input impedance of the four terminal network is $R_b+\delta R_b$. $|\delta R_b| << R_b$. The line current is now

$$I_0'=\frac{V}{R_0+R_b+\delta R_b}\simeq\frac{V}{R_0+R_b}\left(1-\frac{\delta R_b}{R_0+R_b}\right)$$

$$I_0'\simeq I_0\left(1-\frac{\delta R_b}{R_0+R_b}\right) \tag{10}$$

The bridge current at the rebalance is

$$I_1=k'I_0'\simeq k'I_0\left(1-\frac{\delta R_b}{R_0+R_b}\right) \tag{11}$$

The current in the shunt branch of the attenuator is

$$I_3=I_0'-I_1$$

$$=I_0(1-k')\left(1-\frac{\delta R_b}{R_0+R_b}\right) \tag{12}$$

The meter attenuator has a shunting factor of k, so that the power indicated by the meter (P_{rfM}) is

$$P_{rfM} = \frac{R_b I_0}{4}(1+k)I_3$$

$$= \frac{R_b I_0^2}{4}(1+k)(1-k')\left(1-\frac{\delta R_b}{R_0+R_b}\right) \tag{13}$$

The true radio-frequency power is the d-c power flowing into the bolometer at zero adjust minus the d-c power at the radio-frequency balance.

$$P_{rfT} = \frac{R_b}{4}(I_0^2 - I_1^2)$$

$$= \frac{R_b}{4} I_0^2 \left[1 - k'^2\left(1-\frac{\delta R_b}{R_0+R_b}\right)^2\right]$$

$$P_{rfT} \simeq \frac{R_b I_0^2}{4}\left[(1-k'^2) + \frac{2\delta R_b k'^2}{R_0+R_b}\right] \tag{14}$$

$$\text{Percentage error} = \frac{P_{rfT} - P_{rfM}}{P_{rfT}} \times 100$$

$$= \frac{P_{rf} - P_{rfM}}{P_{rfM}} \times 100$$

for small errors.

$$\frac{\text{Percentage error}}{100} = \frac{(1-k')^2 + \frac{2\delta R_b}{R_0+R_b}k'^2}{(1-k')(1+k)\left(1-\frac{\delta R_b}{R_0+R_b}\right)} - 1$$

and since $\delta R_b \ll\ll R_T$, and $1+k \cong 1+k'$

$$\frac{\text{Percentage error}}{100} = \frac{k'-k}{1+k} + \frac{\delta R_b}{R_0+R_b}\left[1+\frac{2k'^2}{1-k'}\right]\left[\frac{1}{1+k}\right] \tag{15}$$

When $1-k'$ is small, a further approximation may be made:

$$\frac{\text{Percentage error}}{100} = \frac{\delta R_b}{R_0+R_b} \cdot \frac{2k'^2}{(1-k')(1+k)}$$

$$= \frac{\delta R_b}{R_0+R_b}\,\frac{k'^2}{(1-k')} \tag{16}$$

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No Discussion

A Broad-Band Signal Generator

THOMAS P. HAHN
NONMEMBER AIEE

JOHN EBERT
NONMEMBER AIEE

W. A. LYNCH
NONMEMBER AIEE

Synopsis: Solutions of some of the problems arising in the development of a broad-band signal generator are presented. Three internal cavity, reflex klystrons are used to cover the frequency range. Attenuator and wavemeter designs in coaxial radio-frequency cable are discussed as well as attenuator calibration and accuracy of output power as read from a panel control. The method of pulsing the klystrons is described with emphasis on the problems involved in obtaining very short rise and decay times.

CALIBRATED sources of signal power are required whenever any particular region of the spectrum is being developed. The greater the frequency range covered by any signal generator, the more useful will the unit be as a piece of laboratory equipment. As in all completely new developments, some compromise between wide frequency coverage and proportionally increased bulk and complexity must be established. The present character of developmental work in the microwave region of the spectrum made it desirable to engineer a signal generator covering the frequency range from about 4,000 to 10,000 megacycles per second.

The design and construction of such a signal generator is described in this paper, and the solutions to some of the major problems involved in obtaining the desired performance are discussed.

The complete signal generator is shown in Figure 1. The unit is self-contained with all controls calibrated to an accuracy consistent with present-day requirements. It contains a built-in frequency meter, a meter for constant monitoring of the continuous wave output power, a precision attenuator, a pulse generator, and voltage stabilized power supplies. The d-c power supplies which provide the various voltages are housed in the lower two racks. The pulse generator and delay circuits are contained in the third rack, and the uppermost assembly, a radio-frequency shielded chamber, contains all the microwave components as well as the pulse keyer.

The complete spectrum from 4,200 to 10,300 megacycles per second is covered by three velocity modulation tubes of the reflex type whose outputs are fed through a coaxial line switch to a matching transformer. The switch provides the independent selection of the output from one of the three oscillators, and simultaneously completes the switching by applying the d-c voltages to the desired tube.

A block diagram of the radio-frequency circuit is shown in Figure 2. The capacitative probe in the coaxial line between the matching transformer and attenuator supplies a small fraction of the total power to the frequency meter. The attenuator is of the waveguide-below-cutoff type with matched resistive loops of novel design. Radio-frequency energy flowing into the attenuator is monitored by the launching element which constitutes, in addition, one arm of an a-c bridge. Associated with the bridge is a stable amplifier whose detected output may be observed on a panel meter.

Provisions are made for both continuous wave and pulse operation; and, in the latter case, wide ranges of pulse duration, rate, and delay with respect to a synchronizing trigger are obtainable.

Microwave Circuitry and Components

A survey of existing oscillator tubes operating between 4,200 and 10,300 megacycles per second capable of supplying approximately 100 milliwatts of energy to the attenuator showed that the *2K43*, *2K44*, and *2K39* klystrons could collectively cover the required frequency range. It would have been more desirable to use a single tube requiring less voltage and input power, but no such tube was available. The oscillator tubes selected are normally supplied with narrow frequency range tuners. To facilitate ease of operation, each oscillator tube was provided with a broad-band mechanical tuner that permitted satisfactory tuning over the entire range of each tube. Tests showed that if the cavity resonator grids were kept parallel as the tubes were tuned

Figure 1. 4,200 to 10,300 megacycles per second signal generator mounted in a 54-inch relay rack

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THOMAS P. HAHN, JOHN EBERT, and W. A. LYNCH are with the Polytechnic Research and Development Company of Brooklyn, N. Y.

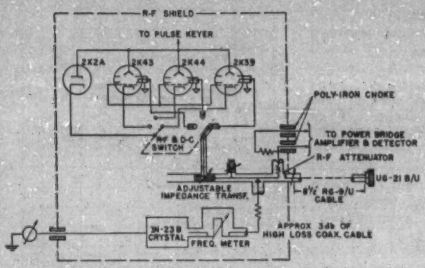


Figure 2. Block diagram of the radio frequency section

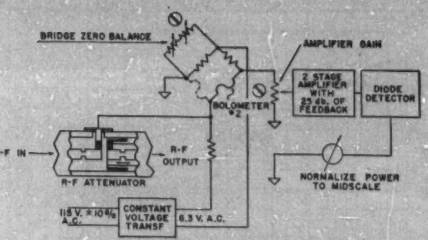


Figure 3. Attenuator and power meter circuit

over their respective frequency ranges, sufficient power output would be available.

Since three oscillator tubes were utilized to cover the frequency range 4,200 to 10,300 megacycles per second, some switching arrangement was necessary. A single-pole six-throw radio-frequency coaxial line switch was modified to apply the minus 1,250 volt beam voltage to the desired tube as well as completing the radio-frequency connection to the coaxial line system. The reflectors and control grids are left in the circuit at all times.

The power output of a klystron is dependent upon many factors, one of which is the complex impedance of the output circuit. A matching transformer consisting of a single variable length stub and coaxial line stretcher was used to vary this impedance. By using dielectric rods to change the electrical lengths in the matching transformer, it was possible to eliminate all sliding contacts.

The radio-frequency energy is fed into the attenuator which is of the waveguide-below-cutoff type supporting the TE_{11} as the principal mode. Small metal-coated glass tubes having a d-c resistance of approximately 50 ohms are used as power launching and pickup elements. Because of the resistive nature of the metallic films on these elements, and because of their small size, broad-band impedance matching techniques could be utilized. The resistance of these films are a function of the radio-frequency input power, and by making the launching element constitute one arm of an a-c bridge circuit, a broad-band matched power monitor was obtained. This power monitor or bolometer provides the sole means of normalizing the power output calibration.

Figure 3 illustrates the radio-frequency power bridge, amplifier and diode rectifier. Bolometer 2, the temperature compensating element, is identical to the launching bolometer element and is soldered to a large mass of metal to reduce transient temperature fluctuations. Because of the possibility that the klystrons may generate average power up to 0.75 watt at certain frequencies, the platinum-film bolometer elements were designed to withstand one watt average power. Figure 4 shows the power resistance characteristics of a typical element. Changes in the resistance of the bolometer element due to changes in the radio-frequency power input to the attenuator produce an unbalance of the bridge which is amplified and detected. The output of the detector is indicated on a panel meter.

The optimized design of the fixed compensating elements for minimum variation of the impedance of the pickup and launching loops is shown in Figure 5. The voltage standing wave ratio (VSWR) of the output circuit of the generator was measured. The output circuit included a *UG-21B/U* connector and $8^1/_2$ feet of *RG-9/U* cable. The VSWR was found to fluctuate with small changes in frequency as reflections from the load and connector alternately add and cancel. The peak values of these fluctuations are plotted in Figure 6 and show that at 5,350 megacycles per second the combined VSWR reached a maximum of 2.2. Figure 7 shows the average VSWR of the over-all output circuit of the generator plotted against frequency. The VSWR of an average type *B* connector, and the VSWR of the pickup loop which had been computed from the data are plotted on the same graph.

Tests were made to determine the effect of the rapid fluctuation in output impedance with frequency on the output power to a matched load. The output impedance in the frequency band from 5.82 to 6.31 kilomegacycles per second varied through eleven complete cycles (owing to the $8^1/_2$ feet of cable) with eleven maxima averaging approximately 1.56 to 1.60 in VSWR with an average minimum VSWR of approximately 1.10.

A bolometer in a matched mount having a 1.07 VSWR over this band was then used as an output power indicator. The power input to the attenuator was held constant while the frequency was varied over the specified band. The measured output power gradually increased by about 0.6 decibel as the frequency increased. This change compares very favorably with the change in insertion loss of the attenuator over this band.

Since it was at first expected that the power output would vary cyclicly with the output impedance, the theoretical aspects of the problem were examined using the diagram shown in Figure 8. Y_c represents the mismatch admittance of the output connector and Y_o the matched load admittance. This output circuit is

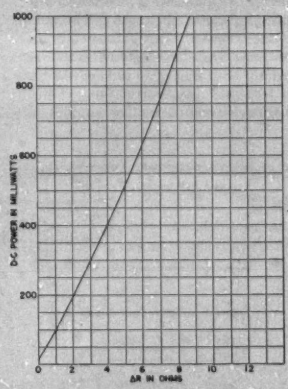


Figure 4. Power-resistance characteristics of bolometer elements

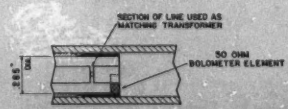


Figure 5. Details of matching transformer

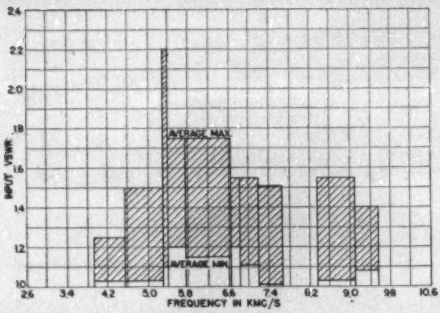


Figure 6. Bands of rapidly fluctuating voltage standing wave ratio in output circuit

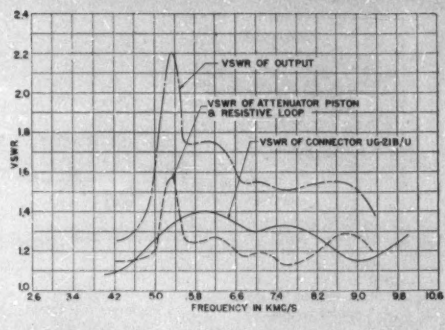


Figure 7. Individual voltage standing wave ratio curves of components in output circuit

separated from the pickup loop, Z_g and E_g, by a long length of cable, L. The fluctuating impedance previously mentioned was measured looking into the line at point a, taken to the right of the connector. If Thevenin's theorem is applied at plane a, it must be borne in mind that, because of the presence of Y_c between the pickup loop and point a, both E_{oc} and Z_{in} for the Thevenin circuit will change with frequency. A fixed point at b would be more desirable, since E_{oc} would then remain constant even though Z_{in} would change with frequency, because of the changing electrical length between the pickup loop and point b. An even more desirable condition would be to shift the point b with frequency to keep the electrical length and hence Z_{in} and E_{oc}, constant. This has been done for the representation in Figure 9.

The data given in Figure 9 represent the worst possible combinations of impedance that have been experienced. Z_{in} is the impedance of the pickup loop as seen from point b, which was selected so that Z_{in} was real and maximum. Z_L is the combination of Y_c and Y_o as seen from point b at two discrete frequencies, when Z_L is real and minimum and when Z_L is real and maximum. Point b was shifted as the frequency changed so that Z_{in} (and E_{oc}) remained constant. Since the lengths involved are electrically long, the 180 degree phase shift in Z_L can take place at two frequencies very close together; therefore Z_g and Y_c can be considered to remain essentially constant. P_1 and P_2 are the values of power delivered to the load under the two phase conditions. A is the ratio of these two power levels expressed in decibels.

Over the frequency range for which tests of power variation with frequency were actually made, the equivalent circuit of Figure 10 applies. Hence, over this range, the error in power output to a matched load would be small, approximately ±3 per cent. This was partially obscured in the experimental test by the changing insertion loss.

After taking precautions in the attenuator design to eliminate or reduce to an absolute minimum the possible errors caused by rotation of the pickup loop, impedance mismatch, and radio-frequency choking at the launcher-element, the following factors remained and had to be calibrated:

1. Changing insertion loss of the attenuator with frequency over the range 4,200 to 10,300 megacycles per second.
2. The presence of higher order modes, other than TE_{11}, in the vicinity of the launching and pickup elements.
3. Change in the rate of attenuation with frequency.

The change in insertion loss was compensated by the use of a multiple index on the attenuator dial. A correction for changes in attenuation slope with position and with frequency was made by calibrating the main dial at mid-band and supplying a correction curve for the other frequencies. Figure 12 shows the nature and amount of this correction.

The maximum error in the output power, calibration from the generator is less than ±2 decibels. A summation of the possible errors is as follows:

	Decibels
1. Internal Bridge, Amplifier and Detector Error—This includes ±10 per cent variation in the line voltage, the changing of the mutual conductance of the amplifier tubes with age or by replacement with new tubes, errors in the bridge components, and faulty zero adjustment	±0.35
2. Impedance Mismatch—A discussion of this error is given in the preceding pages	±0.32
3. Calibration Error—The result of reading errors and equipment inaccuracies was estimated to be not greater than	±0.50
4. Bore Error—Even though great care was taken in the boring and final reaming of the cutoff waveguide section of the attenuator, the following possible error must be included in a tabulation of this nature	±0.15
5. Resetability—The dial setting error was estimated as a result of experience gained in calibrating the set to be approximately 0.8 degrees (human error in setting dial)	±0.11
6. Irregularities in the Output Coaxial Cable—Reflections set up in a cable because of manufacturing imperfections and because of twists and bends. Experimental results have shown this error to be approximately	±0.20
7. Temperature—The cutoff attenuator and power measuring bridge were fully compensated for the ordinary laboratory temperature variations; hence no error is placed on this variable.	
8. Backlash—The measured value for this error was ±0.9 degrees which corresponds to	±0.27
Possible Total Error	±1.90

This error is possible only if all of the

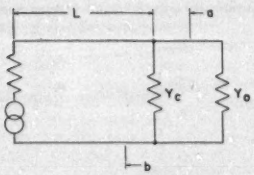


Figure 8. Equivalent circuit A

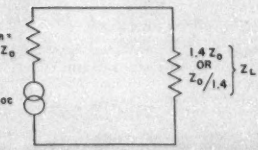


Figure 9. Equivalent circuit B

$P_1 = 0.1588\,E_{oc}^2$
$P_2 = 0.1368\,E_{oc}^2$
$A = 0.64$ db.
$= \pm 0.32$ db.

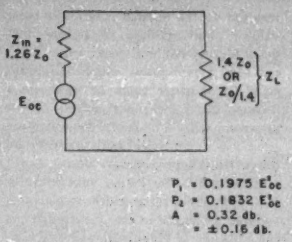
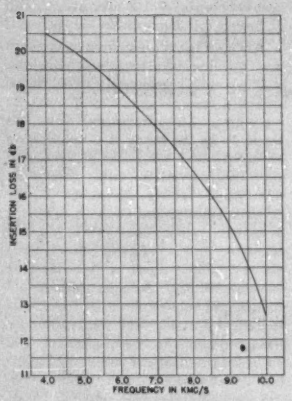
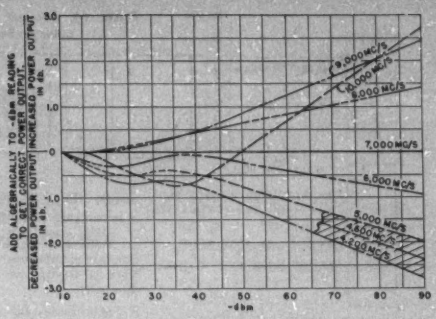


Figure 10 (left). Equivalent circuit C

Figure 11 (below left). Insertion loss for TE_{11} attenuator 0.3295-inch bore

Figure 12 (right). Power output correction curve





component errors are maxima and of such sign as to add. The probable error is, therefore, considerably less than this value.

Because of the nature of the tubes it was impossible to calibrate the oscillator dials in frequency with the desired degree of accuracy. A search frequency meter was, therefore, coupled to the coaxial line. A capacitative probe was used for coupling the frequency meter to the main radio-frequency line. The position of the probe in the radio-frequency circuit is shown in Figure 2. The frequency meter used is a concentric-line resonator with a variable quarter-wave plunger whose drive shaft is geared to a panel control and calibrated dial. Resonance is indicated by the transmission of energy through the cavity to a rectifying crystal, the output current of which is indicated by a panel meter. The coupling into and out of the cavity is accomplished by means of inductive loops. Figure 2 shows this coupling arrangement and the general form of the frequency meter. The oscillation frequency of the klystron tube in use can be measured to an accuracy within ±0.75 per cent.

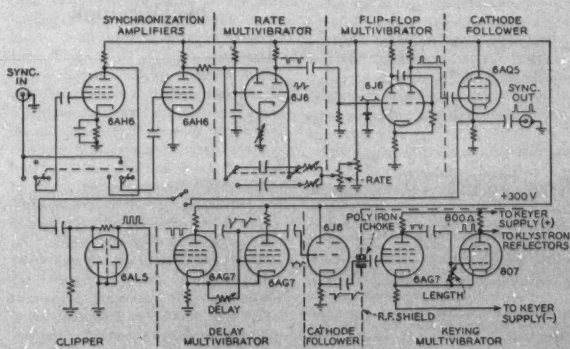
All the radio-frequency components as well as the keyer circuit were mounted in a large shielded radio-frequency chamber to minimize stray leakage. All seams and joints of this box, excluding the cover, were riveted and welded. All drive shafts were made of insulating materials and were brought out through waveguide-below-cutoff assemblies. The attenuation of each assembly was greater than 100 decibels. Cooling vents were necessary and consisted of honeycombed cutoff waveguide. All electrical leads entering or leaving the shield were filtered by using polyiron chokes whose attenuation at the lowest frequency employed is approximately 100 decibels. The cover was made from 1/8-inch sheet aluminum and was securely fastened in place. A gasket of monel metal fabric was used to prevent leakage at the cover. To insure optimum results from the gasket, the contacting surfaces were given a coating of colloidal silver paint. The same method was used to seal the attenuator opening. All assemblies mounted on the walls of the shielded chamber were designed with wide flanges for clamping purposes. To insure leak-proof mountings, the joining surfaces were kept clean and were given a coat of colloidal silver paint.

Tests made at 9,000 megacycles per second to determine the effectiveness of the shielding, showed the maximum radio-frequency leakage to be approximately 90 decibels below one milliwatt.

Pulse Modulation

The requirements placed on the characteristics of the radio-frequency pulse were exceedingly rigorous. The rise and delay time of the radio-frequency pulse were held to approximately 0.1 microsecond. The peak radio-frequency power under all conditions of pulse duration and rate is maintained the same as the normalized continuous wave power, by designing the pulse keying modulator so that the reflector voltage during the pulse is

Figure 13. Simplified schematic diagram of pulse generator and keyer



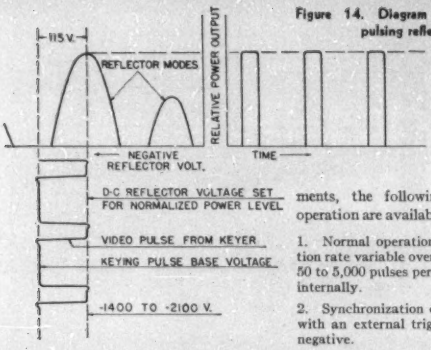


Figure 14. Diagram showing method of pulsing reflector voltage

returned to the same value as that for continuous wave operation at which the initial radio-frequency adjustments were made. The circuit diagram of the complete pulse generator and keyer is shown in Figure 13.

The pulse generator itself consists of two synchronizing amplifiers, a free-running multivibrator determining the pulse repetition frequency, a synchronizing output trigger circuit and cathode follower, a delay one-shot multivibrator and a cathode follower to drive the keying multivibrator. Because all panel controls were calibrated directly to ±10 per cent, interaction between stages in this unit was eliminated through the liberal use of decoupling and voltage stabilizing circuits. The pulse generator has a repetition rate range from 50 to 5,000 pulses per second and a pulse delay range from 1 to 100 microseconds with reference to the output synchronizing trigger. Through the use of switching arrangements, the following modes of pulse operation are available:

1. Normal operation, namely, the repetition rate variable over the entire range from 50 to 5,000 pulses per second and generated internally.
2. Synchronization of the pulse generator with an external trigger either positive or negative.

The reflectors of the three klystrons as well as the plate of the clipper diode, Figure 2, are connected together, and the video pulse is applied at this point. The total parasitic capacitive load on the keyer is about 40 micromicrofarads. Because the rise and decay times are held to a minimum, this capacity makes it necessary to develop the pulse across a very low impedance. The final circuit makes use of an 800-ohm resistor in the plate circuit of the 807 section of the multivibrator, Figure 13.

The method of pulsing the klystrons consists of applying a positive pulse whose base voltage is negative with respect to the reflector voltage, thereby causing the reflector voltage to shift to a value which stops oscillation in the time duration between pulses. This is shown in Figure 14. The degree by which the reflector is driven negative is equal to the peak pulse voltage; hence when the pulse appears across the load resistor, the reflector voltage is restored to the normal continuous wave value. This method of pulsing produces a steep rise and decay time on the radio-frequency pulse which is always equal to or better than the video pulse because of the clipping action of the klystron itself. The final radio-frequency pulse was symmetrical with the rise and decay time approximately equal to 0.1 microsecond. The keying multivibrator also controls the pulse width by placing a variable resistance in the grid return circuit of the 807. The pulse width is variable between 0.5 and 30 microseconds and is directly calibrated within ±10 per cent.

When switching over from continuous wave to pulse operation, it was found that a serious shift in frequency resulted, which caused a marked degrading of the pulse waveform. This frequency shift was found to be the result of a temperature change due to lower power dissipation in the klystron under pulse conditions. The smaller power dissipation in the klystron when pulsed results in a change of the cavity dimensions and the klystron resonator grid separation. By holding the d-c power input to the klystron constant through the adjustment of the control grid voltage on the klystron so as to return the d-c beam current under pulse conditions to the continuous wave value, the defects under pulsing conditions can be eliminated. Tests showed the frequency shift between pulse and continuous wave operation less than 0.09 per cent when the input power was kept constant. Under these conditions the pulse waveform is essentially the same throughout the radio frequency range, and the peak power during pulses is the same as that under continuous wave calibration conditions.

No Discussion

Microwave Frequency Measurements and Standards

BENJAMIN F. HUSTEN
NONMEMBER AIEE

HAROLD LYONS
MEMBER AIEE

IN THIS PAPER a survey is given of the problem of microwave frequency measurement and standardization from the point of view of the method adopted at the National Bureau of Standards. The bureau's microwave frequency standard, methods of measurement, and accuracy of calibrations are discussed. A brief preview of possible future developments in this field is also given to indicate some of the lines of attack being considered for the extension and diversification of microwave frequency measurement methods and the application of these techniques to related problems.

National Bureau of Standards Microwave Frequency Standard

The National Bureau of Standards microwave frequency standard was designed to provide continuous frequency coverage from approximately 300 megacycles per second up to at least 30,000 megacycles per second. There is also provision for the generation of spot frequencies of highest precision at approximately 1 per cent intervals over the same band of frequencies. The completed unit was designed to be comparatively easy to operate and maintain, to be as free as possible of all spurious frequencies, and to provide sufficient power over the entire range so it would be possible to calibrate most microwave frequency meters without resorting to external voltage sources. The establishment of this microwave frequency standard was initiated by a request of the Joint Communications Board of the United States Joint Chiefs of Staff in 1944. The unit was put into operation in June 1945 with the help of the Massachusetts Institute of Technology Radiation Laboratory.

In order to keep the power requirement to as low a value as possible, sensitive spectrum analyzers or superheterodyne receivers with panoramic adapters are used as indicating equipment. The actual power available up to at least 26,000 megacycles per second is more than sufficient to perform most calibrations even though 10 decibel isolating pads are used on each side of the meter being calibrated.

The complete microwave frequency calibration equipment except the 100-kc primary standard is contained in one room which is well shielded and provided with temperature and humidity control equipment.

The standard microwave frequencies are obtained from the National primary standard of frequency at 100 kc through a system of frequency multiplication, frequency conversion, and harmonic selection. The National primary standard consists of nine quartz-crystal oscillators which are compared automatically with each other and with Naval Observatory time. The absolute value of the frequency of any one of these oscillators is known at all times to within one part in 100 million.

In Figure 1 is shown a picture of the complete microwave frequency generator. The center rack contains the multipliers, decade generator, frequency converters, and variable frequency generators necessary to obtain and multiply the desired frequency to 30 megacycles per second. The top units of the rack on the left are, respectively, the high-frequency multiplier stages and high-frequency filters. Immediately below these units is the spectrum analyzer with interchangeable microwave plumbing. This equipment is set up for the calibration of a 9,000-megacycle frequency meter. The equipment in the right-hand rack is used to determine precisely the frequency of the variable low-frequency oscillators when they are being used.

Additional equipment not shown in the picture but used during some calibration includes superheterodyne receivers and panoramic adapters which cover the range from 300 to 3,000 megacycles per second and various voltage generators which cover a considerable part of the spectrum between 300 and 30,000 megacycles per second.

Continuous Coverage System

The block diagram in Figure 2 shows the microwave standard set up for continuous coverage. This 100-kc output from the primary standard, after passing through a distribution amplifier, is multiplied to 7,500 kc. This frequency then is added to the output of one of two precision variable oscillators to give an output which is variable from 9.5 to 10.3 megacycles per second. Although, for simplicity, it is not shown in the block diagram, it is possible to add a fixed frequency to the output of either of these oscillators through the equipment used for spot frequency generation so as to obtain a frequency range from 2.0 to 3.0 megacycles per second, which when added to the 7.5 megacycle frequency results in an output variable from 9.5 to 10.5 megacycles per second. In order to reduce the level of undesired modulation products, particular attention was given to the design and construction of the frequency converter where the output of the variable oscillator is combined with the 7.5-megacycle frequency obtained from the primary standard.

The output of the converter is passed through four consecutive frequency triplers with outputs available in the vicinity

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BENJAMIN F. HUSTEN and HAROLD LYONS are both with the central radio propagation laboratory, National Bureau of Standards, Washington, D. C.

Figure 1. Microwave frequency standard

of 30, 90, 270, and 810 megacycles. Each of these stages has a tuning range of slightly greater than 10 per cent so as to accommodate only the range of variable frequency, making it impossible to tune to any except the proper harmonic. The output from any one of these four tripler stages can be impressed across a nonlinear element such as a crystal rectifier which will generate higher-order harmonics. This part of the equipment will be discussed in a following section. The frequency stability of any one of the harmonics will be directly dependent upon the output of the 10-megacycle frequency converter which, in turn, is about 75 per cent dependent upon the primary standard and only 25 per cent dependent upon the precision oscillators. These oscillators are equipped with micrometer dials which give a change in frequency of about five parts in a million per dial division. The instantaneous frequency of the oscillators can be measured to within one cycle by means of the equipment on the right of the block diagram. This equipment consists of a frequency divider, harmonic generators, a receiver, a comparison oscilloscope, and an audio-frequency interpolation oscillator.

Harmonics controlled directly by the primary standard are produced at intervals of 10 kc in the receiver. The unknown frequency and one of these harmonics beat together to produce a beat note which can be measured to within a cycle by means of the interpolation oscillator and the comparison oscilloscope. The interpolation oscillator can, in turn, be calibrated directly against the primary standard.

When the frequency of the variable oscillator is known to within one cycle, the 10-megacycle output and the resulting microwave harmonic are known to within one part in 10 million.

Spot Frequency System

At times a calibration is not desired at any particular frequency but at several points within a range of frequencies. In order to easily obtain such a series of precisely known frequencies the spot frequency generating equipment is used. Figure 3 shows a block diagram of the microwave standard set up for such use. One very apparent simplification is obvious from a comparison with the general coverage system as shown in Figure 2. No auxiliary frequency measuring equipment is needed since the frequency is at all times dependent directly upon the primary standard. The precision oscillators now are replaced by a system of multipliers and converters, called the decade generator, which provides an output adjustable in 100-kc steps from 2.0 to 3.0 megacycles per second. This is accomplished by adding to a 2.0-megacycle frequency the output of any one of a series of frequency multipliers which provide outputs every 100 kc from 100 to 1,000 kc. This frequency then is added to the 7.5-megacycle output and passed through the frequency multipliers as in the previous case. Any one of the outputs is now adjustable over an approximately 10 per cent range at intervals of 1 per cent. Since these frequencies are derived directly from the primary standard, they are known to within one part in 100 million.

Fine Tuning System

An additional arrangement of the equipment is possible, namely, the fine tuning arrangement. This consists of a precision oscillator variable between 500 and 600 kc which can be mixed with the outputs of the decade frequency generator and multiplier to give continuous coverage from 2.0 to 3.0 megacycles per second. The stability of the output from the 10-megacycle converter stage with this system is 95 per cent dependent upon the primary standard and about 5 per cent dependent upon the oscillator. The advantage of this system over the previous continuous coverage system is that each dial division of the oscillator changes the frequency about one part per million as compared to the change of five parts per million of the other oscillators. The frequency of the oscillator is determined by the interpolation equipment as in the previous case and the accuracy of the final frequency is within one part in 10 million.

Generation of Higher-Order Harmonics

Although the output from the microwave frequency standard is tunable over a relatively narrow frequency range, continuous frequency coverage can be obtained through the overlapping of harmonics. With the 10 per cent band width being used, harmonics of the output above the tenth overlap and provide continuous coverage. The 30-megacycle output provides continuous coverage from 285 megacycles, the 90-megacycle output from 855 megacycles, the 270-megacycle output from 2,565 megacycles, and the 810-megacycle output from 7,695 megacycles. The output of any one stage must be used up to the 30th harmonic before the harmonics of the succeeding stage begin overlapping. The power level of the 30th harmonic of any one of the stages is sufficient for frequency calibration purposes.

In order to generate these high-order harmonics the output from any one of the four frequency multiplier stages is impressed across a crystal diode mounted in a coaxial line or a section of wave guide, whichever the case may be. Figure 4

shows one of these crystals mounted in a section of 1-inch-by-1/2-inch wave guide intended to operate in the range from 8,500 to 10,000 megacycles. A typical crystal is shown in Figure 5 and a schematic of the crystal is shown in Figure 6. The crystal is mounted in the wave guide with appropriate tuners so it not only generates the harmonics, but also serves as an antenna for the efficient radiation of the microwave energy down the guide.

The power which these crystals can handle without overloading or burning out is limited. Some of the crystals designed to operate in the vicinity of 1,000 megacycles can handle several watts of power without being damaged, but most of the higher frequency crystals can handle only a few tenths of a watt.

Measurements made on the strength of the harmonics available from a *1N23* crystal diode indicate that with a power input somewhat less than 0.4 watt from the 810-megacycle stage the power level of the 11th harmonic at 8,910 megacycles is about 80 microwatts. The power available at the 30th harmonic of the same stage from a *1N26* crystal diode at 24,300 megacycles is about 0.05 microwatt. Since the sensitivity of the receiver is about 10^{-4} microwatt, there is sufficient power to perform most calibrations.

When a wave guide is used to propagate these higher-order harmonics, no difficulties are ever encountered with lower harmonics or with the fundamental itself since the wave guide transmission line is an excellent high-pass filter, passing only those frequencies which are above the cut-off frequency. When coaxial line is used for the transmission of the energy, it is sometimes necessary to employ tuned stubs or filters in the line to eliminate the stronger lower-order harmonics.

Receiving Equipment

Since superheterodyne receivers used with panoramic adapters are well known, no discussion will be given of this equipment which is used below 2,700 megacycles. The spectrum analyzer and its use, however, may warrant a slight description. The use of and precautions needed with this receiver will apply in general to the panoramic adapter combination.

The spectrum analyzer is essentially a superheterodyne receiver with a frequency-modulated local oscillator. A block diagram of a typical spectrum analyzer receiver is shown in Figure 7. The local oscillator, a klystron tube, is frequency-modulated by means of a saw-tooth wave. This same saw-tooth wave is impressed upon the horizontal plates of a cathode-ray tube. The video output of the receiver is impressed on the vertical plates of this tube. Whenever the difference between the input frequency and the frequency of the local oscillator is equal to the intermediate frequency of the receiver, a pip will appear on the screen of the cathode-ray tube. The height of this pip will be a function of the input power and the position on the screen will be a function of the input frequency and the center frequency of the local oscillator. There will be two such pips for each frequency although they may not be on the screen simultaneously. These pips correspond to instantaneous local oscillator frequencies above and below the input frequency. When the spectrum analyzer is used to compare two frequencies, it is possible for the two to appear to coincide when they are separated by twice the intermediate frequency. When the two frequencies are in proper coincidence the two pips above the local oscillator frequency will coincide as will the two below the local oscillator. Figure 8 shows a close-up view of the spectrum analyzer with one of the interchangeable wave guide assemblies used to cover the various frequency ranges corresponding to each wave guide size.

Measurement of Microwave Frequencies

In order to expedite the setting of the microwave frequency standard, tables to the frequencies of the various harmonics versus the variable frequency input into the 10-megacycle converter have been

Figure 2. Microwave frequency standard continuous coverage with adjustable oscillators

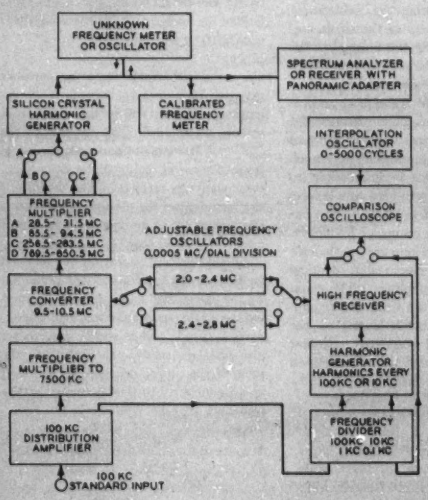
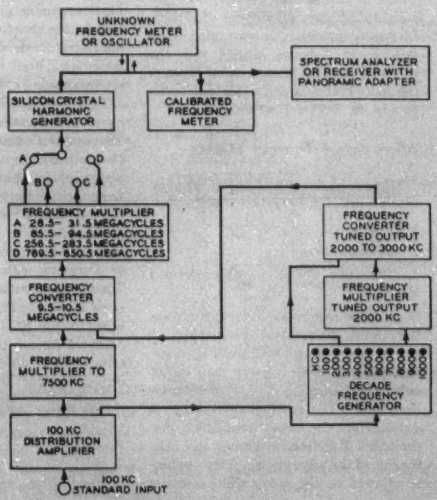


Figure 3. Microwave frequency standard spot frequency coverage



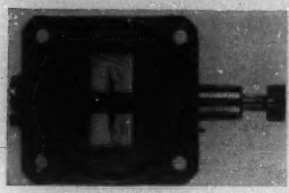


Figure 4. Crystal diode mounted in wave guide

prepared for every 100-kc step from 2.0 to 3.0 megacycles. The frequency to be measured is tuned in on the spectrum analyzer and an approximate value of the frequency is obtained from a calibrated frequency meter. The frequency standard then is set up for the proper frequency by reference to the tables. A pair of pips should appear on the screen of the receiver and by adjusting the power level of the standard or of the generator being measured they can be made equal in amplitude. At this point a check should be made to insure that the two frequencies are approximately equal and are not separated by twice the intermediate frequency. By bringing the frequencies into coincidence and increasing the dispersion it is possible to observe the beat notes on the screen. These beat notes appear as a filling-in of the response curve on the screen. It is possible with this method to determine the frequency of an oscillator to within one part in one million, which is considerably better than the stability of most microwave oscillators. If greater precision is warranted, it is possible to dispense entirely with the spectrum analyzer method and to measure the audio beat note of the two frequencies by any one of several methods.

Microwave Frequency Meters

Microwave frequency meters in common use are of three types

1. Coaxial line.
2. Cylindrical cavity.
3. Transition, which is a combination of 1 and 2.

Since these meters are resonant cavities the terms frequency meter, resonant cavity, and cavity often are used interchangeably. These meters may be of either the transmission type or the reaction type. The reaction-type meter is coupled into the main transmission line through a T section or through an iris in the side of the wave guide. The coupling is adjusted so the meter will present a high impedance in series with the wave guide when it is tuned to resonance, thereby decreasing the power transfer of the guide. The meter is adjusted to resonance by tuning for minimum output.

The transmission-type meter is inserted directly into the line and the energy must pass through the meter. Resonance in this meter is indicated by maximum power transfer. In Figure 9 are shown some typical microwave resonant-cavity frequency meters.

Accuracy of Calibration

Although the frequency from the microwave frequency standard is known to at least one part in 10 million, it is practically impossible to certify a frequency meter to this accuracy. Many factors enter into the exact determination and certification of the resonance frequency which limit the accuracy of the usual meter to about 1 part in 100 thousand or less.

Mechanical inaccuracies such as dial backlash, loose or faulty mode dampers, creepage of the metals or dielectrics, and lack of ruggedness are obvious limitations to the accuracy but are not the only factors which must be considered. When a precise determination of the resonance frequency is desired, such variables as temperature, humidity, and pressure may have to be considered. In addition to these factors the amount of external reactance which is coupled into the frequency meter will also affect the resonance frequency and must be taken into account or made negligible during the calibration. Finally, the sensitivity of the resonance-indicating device and the finite Q of the meter itself limit the accuracy to which resonance can be determined.

From rigorous field theory it is possible to develop equations which describe the behavior of a resonant cavity in terms of the electric and magnetic fields at chosen planes of reference. It is possible to write these equations in very simple form for the behavior of a cavity in the immediate vicinity of a resonance frequency. The equations given here are for the case of a cavity with electrically identical input and output couplings. The equations in this form are from the unpublished notes of David Kerns of the National Bureau of Standards. Similar expressions in different forms have been derived by other investigators.[1]

$$I_1 = I_2$$

$$V_1 = \frac{2Q_L}{\sqrt{T}}\left(\frac{1}{Q_0} + \frac{2j\,\Delta f}{f_0}\right) I_2 + V_2$$

I_1 and V_1 are, respectively, the linear measures of magnetic and electric fields at the input plane of reference. I_2 and V_2 are similar measures at the output plane. The physical meaning of the equations is given in essence by the fact that $V_1 I_1^*$ is the input power; $V_2 I_2^*$ is the output power from the cavity.

Q_0 is the unloaded Q of the cavity.
Q_L is the loaded Q measured with reflectionless loads at the planes of reference.
T is the ratio of the output power to the incident power at resonance and with reflectionless generator and load.
f_0 is the resonance frequency and
$f = f_0 + \Delta f$
I^* is the complex conjugate of I.

Any number of circuits can be imagined whose terminal voltages and currents are connected by these equations, the simplest being the complex impedance

$$\frac{2Q_L}{\sqrt{T}}\left(\frac{1}{Q_0} + \frac{2j\,\Delta f}{f_0}\right)$$

in series with a generator and a load. This, or any of the other circuit arrangements which can be derived from these equations, can be called the equivalent circuit of the cavity resonator in the immediate vicinity of a resonance frequency.

The resonance frequency of the transmission-type resonant cavity is the frequency at which maximum power transfer occurs. At this frequency the reactive component in the series impedance becomes zero. Δf in the basic equations becomes zero. The accuracy to which the cavity can be set to resonance is a function of the loaded Q of the cavity and the sensitivity of the indicating system. The higher the Q of the cavity and the smaller the percentage change in output power or voltage which can be observed in the indicator, the greater will be the accuracy of setting. The indicator used at the National Bureau of Standards for most frequency-meter calibrations is a spectrum-analyzer receiver. The height of the response on the oscilloscope screen is approximately proportional to the input power.

In the present notation a matched (reflectionless) impedance is represented by the normalized value unity. By solving the basic equations for a load impedance $V_2/I_2 = 1$, and a source impedance also equal to unity, one finds that the ratio of the power delivered to the load at a frequency f to that delivered at the resonance frequency f_0 is

$$\frac{P}{P_0} = \frac{1}{1 + Q_L^2\left(\frac{2\Delta f}{f_0}\right)^2}$$

where

$f = f_0 + \Delta f$

This ratio is also approximately equal to h/h_0, the ratio of the responses on the oscilloscope screen

$$\frac{\Delta f}{f_0} = \pm\frac{1}{2Q_L}\sqrt{\frac{h_0 - h}{h}} = \pm\frac{1}{2Q_L}\sqrt{\frac{\Delta h}{h}}$$

If $\Delta h/h$ is the smallest fractional change which can be detected in the indicator, then $\Delta f/f_0$ is the uncertainty in the resonance setting. With the indicating equipment now being used it is possible to detect changes in h of about 1 per cent.

For the typical case of a 10,000-megacycle transmission-type frequency meter with a loaded Q of approximately 10,000, the resonance frequency can be determined to within ± 0.05 megacycle.

Although this derivation and example is for a transmission-type meter, it has been found that when the same meter is used as a reaction-type instrument, the inaccuracy of setting is but slightly greater than that of the transmission type.

Figure 5. Silicon crystal diode

When greater accuracies are desired, some other method of indicating resonance must be used. It is possible to increase the accuracy by working on the steep sides of the resonance curve rather than at the relatively flat top. One means of accomplishing this is to inject a low frequency into the harmonic generating crystal along with the standard frequency. This low frequency is adjusted so it produces side-band markers on the microwave harmonic at approximately $f_0/2Q_L$ on each side of the carrier. It is possible to suppress the microwave carrier to a considerable extent by adjusting the level of the low-frequency input. If the response of the indicating system is uniform over the band width f_0/Q_L, resonance is indicated when these two markers are of equal amplitude. With this indicating system it is possible to set the frequency meter in the preceding example to within ± 0.02 megacycle at 10,000 megacycles per second.

An instrument capable of great accuracy in the setting of the resonance frequency is the cavity Q meter as developed by the staff of the MIT Radiation Laboratory.[3] By means of a frequency-modulated oscillator the resonance curve of the cavity is displayed on an oscilloscope screen. The same oscillator beating against the microwave harmonic from the frequency standard produces two bright spots on the resonance curve. By adjusting the intermediate-frequency amplifier of the Q meter these spots can be made to appear at the half-power points of the curve.

Cavity resonance is indicated when these spots are symmetrically placed on the curve. It is possible to set the meter to resonance to within a fractional error $\pm 0.01/Q_L$.

Frequency Pulling by External Reactance

During the calibration or use of any frequency meter it is essential for highest accuracy that the meter be isolated from external reactances by means of matched buffers. These buffers, usually resistive pads with an attenuation of about 10 decibels, are placed at both sides of the meter to prevent reactances in the line from "pulling" the resonance frequency of the meter. In calculating the extent of this pulling it is assumed that an impedance with a series reactance component jX (normalized value) appears at the output reference plane. For the moment it is assumed that the generator impedance is unity.

Because of this added reactance the reactive components in the circuit will now total zero at a frequency which is slightly different from the true resonance frequency. Again employing the basic cavity equations, the condition for resonance can be written

$$\frac{j4Q_L}{\sqrt{T}}\frac{\Delta f}{f_0} + jX = 0$$

or

$$\frac{\Delta f}{f_0} = -\frac{\sqrt{T}}{4Q_L}X$$

For applications of this equation to wave guide techniques it is convenient to express $\Delta f/f_0$ in terms of a voltage standing-wave ratio (VSWR), rather than in terms of the normalized reactance. From transmission-line theory it can be shown that the maximum magnitude of normalized reactance for a given VSWR on the line is $(r^2-1)/2r$ where r is the VSWR. Therefore

$$\frac{\Delta f}{f_0} = \pm\frac{\sqrt{T}}{8Q_L}\frac{r^2-1}{r}$$

This frequency shift is the maximum for a given VSWR existing at the output of the meter.

Any reactance at the generator side of the cavity will also cause a frequency pulling. The maximum possible frequency shift will be the sum of the two individual shifts.

Referring to the frequency meter of a previous example, $f_0 = 10{,}000$ megacycles, $Q_L = 10{,}000$, it will be assumed that the insertion loss of this meter is 6 decibels. A VSWR of 2.0 at the output side of the meter can produce a frequency shift of approximately ± 0.1 megacycle. If the meter also sees a similar VSWR at the input the possible frequency shift becomes ± 0.2 megacycle.

When a matched attenuator is placed between the meter and the rest of the circuit, the standing-wave ratio which the meter sees will be considerably reduced. For example, an infinite voltage standing-wave ratio will be reduced to 1.22 through a perfectly matched 10-decibel attenuator. If such attenuators are used with the meter in the previous example, the VSWR will be reduced from 2.0 to 1.07 and the frequency shift will be reduced to ± 0.02 megacycle.

The usual attenuator is not perfectly matched but does introduce standing waves, which may enhance the standing waves from other discontinuities. If the 10-decibel attenuator pads have a VSWR

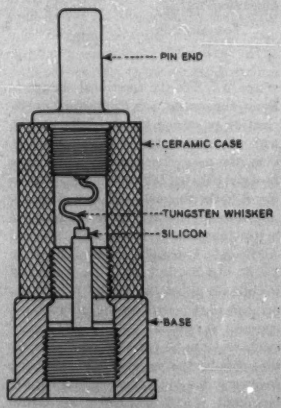


Figure 6. Schematic diagram of a crystal diode

of 1.10 or less, the maximum frequency shifts which can be expected will be

$$\pm 0.16 \frac{\sqrt{T}}{Q_L} f_0$$

(± 0.08 megacycle for the meter of the previous example).

It is apparent that in order to eliminate the possibility of frequency pulling, the meter should be isolated by resistive pads which are in themselves fairly well matched to the line, not only during the calibration but also during the use of the meter. If no suitable attenuators are available, the line should be adjusted so it appears flat looking either way from the frequency meter.

Effects of Temperature, Humidity, and Pressure

Changes of temperature, humidity, and pressure in general will affect the calibration of the cavity-type frequency meter. The actual percentage change in the resonance frequency for a given change in the foregoing factors will depend upon the type of construction and upon the materials in the meter.

The most obvious source of error lies in the change of cavity dimensions due to thermal expansion of the walls. From the principle of electrodynamic similitude[8] it follows that in the case of a perfectly conducting cavity, regardless of the shape of the cavity or the mode of oscillation, an increase in all the linear dimensions by a factor m will decrease the frequency by a factor $1/m$.

Neglecting second-order effects, this can be expressed for small changes in dimensions as

$$\frac{\Delta f}{f} = -\frac{\Delta L}{L} = -\alpha \Delta T$$

where $\Delta L/L$ is the fractional change in linear dimensions, α is the thermal coefficient of expansion for the wall material, and ΔT is the change in temperature. Since the percentage change in frequency for a given change in temperature is proportional to the thermal coefficient of expansion, a cavity made of invar will be much better than one of brass. The thermal coefficient of annealed invar ranges from 1×10^{-6} to 3×10^{-6} while the coefficient for brass is about 19×10^{-6}. If the temperature of the meter during calibration is constant to within ± 0.1 degree centigrade and can be specified to within the same limits, the variations in resonance frequency due to thermal expansions will be about ± 0.02 megacycle at 10,000 megacycles per second for a brass cavity and ± 0.003 megacycle for an invar one. It is possible to introduce compensating elements which will tend to compensate for changes in cavity dimensions so as to keep the resonance frequency constant over a certain range of temperature.

A change in the dielectric constant of the gas in the cavity also will shift the resonance frequency. Changes of temperature, pressure, and vapor pressure in the ambient atmosphere all may affect the dielectric constant of the gas. The extent of this influence will depend upon the type of construction and upon the gas in the cavity. In a sealed unit the type of gas and the moisture content of the gas are determined at the time of construction and remain constant as long as no moisture is condensed out and as long as no leaks develop.

The gas within an unsealed unit is approximately the same as the ambient atmosphere and can be made, for all practical purposes, identical to this atmosphere through a thorough flushing with the ambient air.

A fractional change $\Delta\epsilon/\epsilon$ in the dielectric constant will produce a fractional change in frequency of approximately $\Delta f/f = -1/2(\Delta\epsilon/\epsilon)$. The dielectric constant of air for normal ranges of temperature and pressure is given to a close approximation by

$$\epsilon = 1 + \frac{208}{T}\left(P + \frac{4800}{T} Pw\right) 10^{-6}$$

where P and Pw are respectively the total pressure and the water vapor pressure in millimeters of mercury and T is the temperature of the air in degrees Kelvin.

If the cavity has been filled and sealed with completely dry air or a dry nonpolar gas, Pw will be zero and there will be no change in dielectric constant with respect to temperature. However, if the cavity had been filled and sealed with air containing some water, the change in dielectric constant would depend upon the amount of water vapor present. For temperatures above the dew point of the atmosphere in the cavity, this change is small. The fractional change in frequency for small changes in temperature is approximately

$$\frac{\Delta f}{f} = 1/2 \frac{Pw}{T^2} \Delta T$$

If the temperature falls below the dew point, moisture will collect on the surfaces in the cavity and the frequency error may become quite large.

Changes in external pressure have no effect on the resonance frequency of a sealed cavity unless the pressure difference becomes large enough to distort the cavity.

Unsealed cavities cannot be calibrated accurately because the temperature and humidity of the air in the cavity cannot be specified with great precision. Not only is the accuracy of the unit limited in use because of temperature and humidity variations, but in addition, the initial calibration against a primary standard is limited in accuracy by the same factors. Changes in atmospheric pressure also affect the resonance frequency of an unsealed cavity. The pressure in the cavity however, is identical to the ambient pressure which can be measured with good precision and corrected for, if necessary.

As an example of the magnitude of these variations it will be assumed that the operating conditions for an unsealed invar cavity are 25 degrees centigrade, 50 per cent relative humidity, and 760 millimeters of mercury. A temperature increase of 1 degree centigrade will cause a fractional change in frequency of -3×10^{-6} through thermal expansion of the wall and $+2\times10^{-7}$ through changes in the dielectric constant of the air. An increase of 2 per cent in the value of the relative humidity will cause a fractional frequency change of -3×10^{-6}. A change of 1 centimeter of mercury in the pressure will introduce a fractional change of -4×10^{-6}.

In order for a precision meter to be

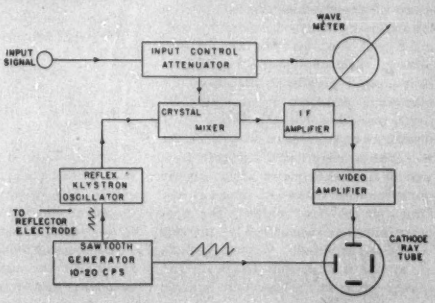


Figure 7. Block diagram of spectrum analyzer

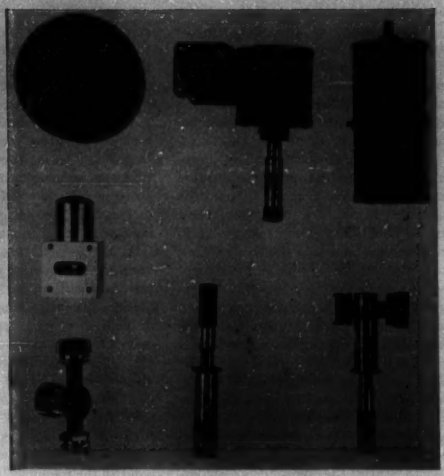


Figure 8 (above). Spectrum analyzer with interchangeable wave-guide assemblies

Figure 9. Microwave frequency meters

relatively free of external effects the design of such a meter suitable for use as a secondary standard should embody the following features:

1. The meter should be rigidly constructed of materials which will not creep or change dimensions.

2. The materials should have a low coefficient of thermal expansion and should exhibit no anomalies in the coefficient of expansion with temperature cycling.

3. Temperature compensation may be employed.

4. The electrical design should be consistent with high Q requirements and there should be no interfering modes of oscillation. Mode suppressors should be electrically and mechanically stable.

5. All moisture should be removed from the cavity including that absorbed in the walls and in the damping material. The cavity should be filled with a completely dry inert gas and sealed.

Conclusion

In conclusion, it may be of interest to consider some possible future developments and applications in the field of microwave frequency measurements. Some of the developments to be mentioned are already well under way.

There is an urgent need for improved cavities having very great stability. One method of attack on this problem is the consideration of cavities made of quartz in order to reduce temperature variations. The quartz is silver plated in order to form a metallic wall for the cavity. A solid quartz cavity, of course, will be impervious to moisture.

Cavities, particularly highly stable ones, can be used to stabilize klystron oscillators by means of a microwave discriminator circuit.[4] This results in a standard-frequency generator having considerable power output. In this way, the cavity is used as an active rather than a passive frequency standard. Such an oscillator can be quite useful in frequency measurements as an interpolation oscillator just as such a unit is used in low-frequency technique. It also has been proposed by the MIT Radiation Laboratory to use such cavity-stabilized oscillators for microwave communication systems in which an enormous number of radio channels are made available in a given range of the frequency spectrum because of the great stability achieved. It would then seem to be possible to use a cavity-stabilized transmitter in much the same way crystal-controlled transmitters are used at lower frequency, except that turning over all the channels is achieved with only one cavity.

By means of resonant cavity techniques and accurate frequency measurement methods, a general method for the measurement of complex dielectric constants is obtained.[5] When a dielectric substance is inserted in a resonant cavity, measurement of the change in resonance frequency and the change in Q or band width of the cavity gives one a measurement of the dielectric constant and the power factor of the substance introduced into cavity. This method gives an extremely interesting way of measuring relative humidity of atmospheric air and of the index of refraction of atmospheric air as needed for the study of microwave propagation. One method of making a microwave refractometer using these principles is to use two cavity-stabilized oscillators which are tuned to zero beat. Introduction of atmospheric air in one of the cavities then changes the frequency of the oscillator and a measurement of the beat note gives the index of refraction directly.

Another application of resonant-cavity frequency measurements is to a new and precise determination of the velocity of radio waves, or determination of the universal constant c by measuring the resonance frequency and dimensions of a cavity with extreme precision. This investigation is being carried out at Stanford University under Professor W. W. Hansen. Here even the effect of finite conductivity of the walls of the cavity must be corrected for by adding the skin depth of penetration of the current on the walls of the cavity to the dimensions of the cavity.

It is becoming more and more urgent to extend the range of the Bureau's microwave frequency standard above 30,000 megacycles per second. This problem is being attacked by development of a lighthouse-tube frequency multiplier circuit which will provide, through the preceding multiplier chain, crystal-controlled output in the 2,400-megacycle range. By development of suitable crystal rectifier mounts, harmonic generation at a useful power level should be possible to 75,000 megacycles or higher. It is also possible to use Sperry klystron frequency-multiplier tubes to obtain crystal-controlled power at 10,000 megacycles at a level of approximately one milliwatt. By harmonic generation it then should be possible to obtain useful outputs to 100,000

megacycles and higher. At these extreme high frequencies it probably will be desirable to introduce new frequency measuring instruments along the lines of optical equipment. In this connection, microwave interferometer and echelon gratings have been built and have become practical.

In the higher frequency ranges, many substances have spectroscopic absorption lines.[6] A familiar example is ammonia gas which has a very strong absorption line at about 24,000 megacycles. This line is so broad due to molecular collisions that, at atmospheric pressure, the absorption is quite strong even at 9,000 megacycles. Many other gases have been shown to exhibit such absorption and it is likely that additional gases will be found at still higher frequencies, in what can be called the millimeter wave-length bands. Under the proper conditions of low pressure some spectrum lines are extremely narrow so that it may be possible to use such lines as frequency standards and even to control the frequency of oscillators or to use the lines in conjunction with cavities. The use of spectrum lines as microwave frequency standards is, in many ways, analogous to the use of optical spectrum lines as length standards by relating their wavelength to the standard meter. A standard-frequency generator like the one described in this paper can be used to measure accurately the frequencies of absorption lines. However, a quite compact standard-frequency generator, for providing precision marker frequencies to be used for microwave spectroscopy measurements, can be constructed by means of a quartz crystal oscillator and frequency multipliers driving Sperry klystron multiplier tubes.

At the present time, it is hoped that microwave frequency standards at institutions other than the National Bureau of Standards can be standardized through utilization of the standard frequency emissions from station *WWV*. This is particularly easy if the microwave frequency standard is of a type similar to that used at the National Bureau of Standards, since it will only be necessary to adjust the quartz crystal oscillator or other oscillator, serving as the starting point of the frequency multiplying chain, to the frequency of *WWV* by means of the proper receiver and intercomparison circuits. In the future, if suitable absorption lines can be found for use as frequency standards, it may be possible to calibrate the various types of microwave frequency standards, wherever located, through the use of spectroscopic frequency standards. At the present time frequency measurements and calibrations of frequency meters or voltage sources can be obtained from the National Bureau of Standards as a calibration service similar to the standards services rendered by the Bureau in other fields of measurement.

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No Discussion

A Standard of Attenuation for Microwave Measurements

R. E. GRANTHAM
NONMEMBER AIEE

J. J. FREEMAN
NONMEMBER AIEE

IN LINE with the program of the National Bureau of Standards for extending the standards of electrical quantities up through microwaves, the establishment of a standard of attenuation was undertaken. For such a standard to be useful, it must be readily adaptable to the calibration of commercial attenuators over the entire frequency range. Also, for self-consistency, such a standard must be capable of being checked by d-c measurements, thus relating the radio-frequency (r-f) standard to the more accurate primary standards.

A standard of any physical quantity consists not merely in the unique specification of that particular quantity, but equally important, in the specification of the operations, or procedures, through which an unknown quantity is measured in terms of it. The purpose of this paper is to describe such a standard, and the preliminary experiments employed in its evaluation and development.

General Description

Because of the extremely wide frequency range over which attenuators must be calibrated, the construction of a series of standard microwave attenuators to cover the entire spectrum, and the use of the direct substitution method of calibration, is impracticable. A modification of direct substitution, namely, the heterodyne or intermediate-frequency (i-f) substitution method, has been developed in various laboratories,[1,2] and permits the comparison of an unknown attenuator, operating at an arbitrary frequency, with a standard attenuator, operating at a fixed frequency. Thus, one standard attenuator, operating at a convenient frequency, may be used to calibrate attenuators over the entire microwave spectrum.

Essentially, the heterodyne method operates as follows. A r-f generator feeds power through an unknown attenuator into a linear frequency converter, which converts the microwave frequency into the intermediate frequency. The converter feeds through an i-f standard attenuator into an amplifier followed by a detector and meter. The unknown attenuator is then removed, and the standard attenuator adjusted to give the same meter reading. The attenuation of the unknown is then equal to the increase of attenuation of the standard.

This method assumes that the frequency converter is linear, that is, that the i-f power from the converter is proportional to the input microwave power. For small enough input power this is true for a crystal converter, and the operating conditions for a prescribed departure from linearity may be determined experimentally.

The range of attenuation which may be measured by the heterodyne method is less than that measurable by direct substitution, since in the heterodyne method the maximum permissible power input into the converter is limited by the advent of converter nonlinearity. However, a combination of direct substitution, using a previously calibrated r-f attenuator, together with the heterodyne method, extends the heterodyne attenuation range to that of the direct substitution method, although with a decrease in accuracy.

Detailed Description of the Equipment Used to Evaluate the Heterodyne Method at X-Band

The block diagram of the equipment used to evaluate the heterodyne method is shown in Figure 1. The r-f generator is a klystron providing at least 250 milliwatts of r-f power. The d-c power supplies for the klystron and amplifying circuits are battery-stabilized, rather than voltage-regulator-tube stabilized, and stabilized, and special shock mountings are used to prevent noise modulation of the r-f signal by the blower used to cool the klystron. A very high degree of amplitude stability is necessary in all of the components of the system, because the output meter of the system is sensitive to a 0.2 per cent (0.01 decibel) change in power level.

The frequency of the r-f generator is measured by a calibrated wavemeter in the spectrum analyzer. The amplitude of the r-f output of the generator is monitored by a unit as shown in Figure 2. A sensitive d-c galvanometer in the power monitor gives a unit scale deflection for a 0.05 per cent (0.002 db) change in power level. Variable attenuator A, in Figure 1, is used to calibrate the power monitor.

The r-f generator klystron is followed by buffer attenuator B which prevents changes in impedance caused by inserting and removing the unknown attenuator from changing the load impedance presented to the klystron. Thus the power output and frequency of the r-f generator are maintained constant throughout a measurement. A twenty-db attenuator provides an adequate amount of buffing, as evidenced by the fact that the introduction of a 10 to 1 voltage standing-wave ratio at transformer A resulted in no change of generator output, as indicated by the power monitor.

When measuring the insertion loss of

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R. E. Grantham and J. J. Freeman are both with the National Bureau of Standards, Washington, D. C.

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Figure 1. Block diagram of the intermediate-frequency method of attenuation measurement

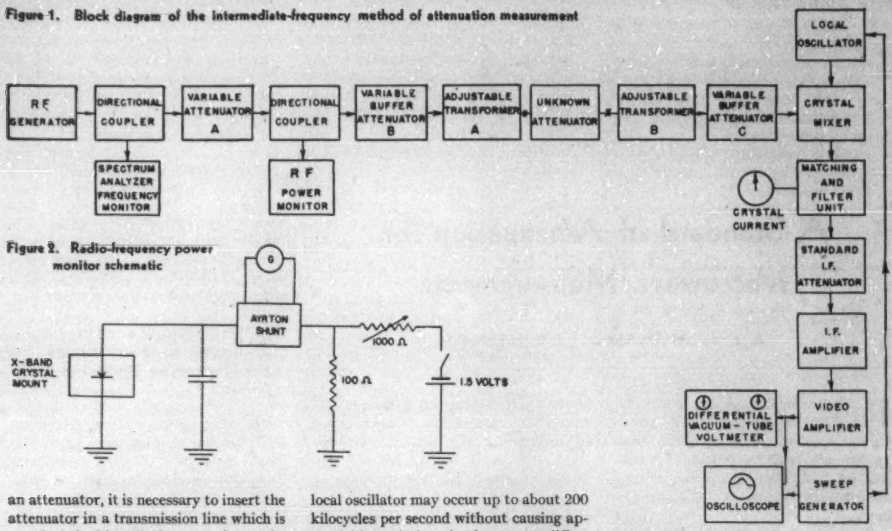


Figure 2. Radio-frequency power monitor schematic

an attenuator, it is necessary to insert the attenuator in a transmission line which is matched looking both ways from the point of insertion. To meet this requirement it is necessary to use adjustable transformers *A* and *B* to match attenuators *B* and *C* respectively as accurately as possible to the characteristic impedance of the wave guide.

Buffer attenuator *C* is needed to isolate the local oscillator from the changes in the impedance of its load which would occur when the unknown attenuator is inserted and removed. The conversion efficiency of the crystal changes appreciably with local-oscillator power. Hence, the local-oscillator power output must be maintained as constant as possible. Twenty db is the minimum attenuation used for buffer attenuator *C*.

The repeller voltage of the local-oscillator klystron is swept by a linear sawtooth voltage so that the beat-frequency output of the mixer sweeps over about 0.5 megacycles per second. The i-f amplifier band width is about 50 kilocycles per second, so the i-f response curve is plotted on the oscilloscope as in the usual spectrum analyzer, but with much more than the usual frequency dispersion. The peak voltage of the pulse on the oscilloscope is measured by a differential vacuum-tube voltmeter, which has a sensitivity of one scale division per 0.2 per cent (0.01 db) change in power level. The reason for sweeping the local-oscillator frequency rather than operating it continuous wave (c-w) is that small shifts in the frequency of the r-f generator or the local oscillator may occur up to about 200 kilocycles per second without causing appreciable variations in the output differential voltmeter. If the local oscillator were operated c-w, the beat frequency could not vary more than about plus or minus 3 kilocycles per second for the same variations in output.

Range of Measurement and Errors of Heterodyne Method

The attenuation range measurable by the heterodyne method is determined by the maximum input power consistent with crystal linearity, and by the crystal noise power. The range of crystal linearity was determined by measuring a fixed value of attenuation as a function of crystal input power, using the apparatus indicated in Figure 3. The measured values of attenuation of a 30-db pad as a function of r-f buffing, and hence crystal input power, are depicted in Figure 4. For 100 milliwatts of r-f input power, it appears that 40 db of buffing attenuation (corresponding to a crystal input power of 10 microwatts) suffices for accuracies of plus or minus 0.02 db.

The crystal noise power was measured to be less than one micromicrowatt. Accordingly, the maximum range of attenuation measurement corresponding to an error of 0.05 db due to crystal noise, is 50 db.

In addition to possible crystal nonlinearity, errors in the heterodyne method may occur through r-f or i-f leakage, mismatch on either side of the r-f or i-f attenuators, frequency and amplitude instability and errors in the i-f standard attenuator. Errors of the i-f standard will be discussed in another paragraph, and the question of errors due to mismatch is too lengthy and detailed to consider at this time. If we neglect the last two sources of error, the over-all accuracy of attenuation measurements with the equipment described herein is estimated to be within plus or minus 0.02 db in the 0-10 db range, and plus or minus 0.2 per cent of the attenuation value in db for the 10-50 db range.

Theoretical Factors Affecting the Choice and Design of the Attenuator

To be suitable as a standard, the i-f attenuator must be able to yield known values of attenuation in terms of an accurately measurable parameter, such as length, or it must be capable of calibration in terms of d-c measurements. First designed by Harnett and Case,[3] the cutoff attenuator, Figure 6, which consists of a hollow tube excited at one end below its cutoff frequency, and a coil or condenser which picks up the attenuated field at the other end, fulfills the former requirement. Since the generated field falls off exponentially with distance from the exciting source, and since the attenuation constant may be computed from the dimensions of the tube, the ratio of any two voltages, or

the attenuation introduced by a given length of tube, is reduced to a measurement of length.

Cross-Section of Tube

However, it is well known that the electromagnetic field generated within the tube consists of the superposition of an infinite number of modes, each of which is attenuated along the axis of the tube as $e^{-\alpha_{nm}z}$, where α_{nm} is a function of the geometry of cross section, and the particular mode. The higher the mode, the greater is its attenuation constant α_{nm}, so that only for large enough values of z do the higher modes become negligible. Obviously, then, the accuracy of the attenuator increases as the ratios of the amplitudes of the higher modes to that of the lowest one decrease, and as the ratios of the attenuation constants of the higher modes to that of the lowest one increase. The comparison of these factors for tubes of circular and rectangular cross section is given in Table I.

Circular Cross Section. The fifth column of Table I enumerates the ratio of attenuation constants, α_{nm}, of the first few modes to that of the lowest mode, for circular cross-section.

From Table I, it is apparent that a measure of purity of mode can be achieved by exciting the TE_{11} mode, and by eliminating the TE_{01}, TE_{21}, and TM_{01} modes through symmetry of excitation. Just as a violin string plucked in the center does not vibrate with even harmonic modes similarly, a proper symmetry of the exciting current distribution in a cutoff attenuator will not excite certain classes of modes. In particular, if one considers the cross section divided into four symmetrical quadrants, and if the current distribution in the right-hand quadrants is the negative mirror image of that in the left-hand quadrants, and if the current distribution in the upper quadrants is the positive mirror image of that in the lower quadrants, then only those modes will be excited for which n, (the mode index indicating angular dependence) is odd, eliminating five of the seven undesired modes listed in the column. However, if one uses an unbalanced generator to excite the attenuator coil, the distributed capacitance of the coil makes the achievement of this symmetry difficult.

The obvious type of exciting symmetry associated with a circular tube is, of course, circular symmetry. In particular, circularly-symmetric capacitative disk excitation, described by Harnett and Case[3] and W. O. Smith[4] will excite only transverse magnetic modes, for which $n=0$; of these the mode with the lowest attenuation constant is the TM_{01}. The danger with this type of attenuator is that any asymmetry will produce a TE_{11} mode which has a lower attenuation constant, and hence, for a large enough value of attenuation, can lead to appreciable error.

Rectangular Cross Section. The first four columns of Table I enumerate the ratios of attenuation constants, α_{nm}, of the first few modes to that of the lowest mode, for several ratios of rectangular cross-section dimensions, a and b. It will be noticed that for $a=b/2$, or less, the unwanted modes decay much faster than for circular cross-section. Also, if the exciting-current distribution is chosen so that to each element of current corresponds its negative mirror image with respect to the plane $x=a/2$, and its positive mirror image with respect to the plane $y=b/2$, then it may be shown that only those modes are excited for which m is even and n is odd. Although this type of symmetry is difficult to achieve at lower frequencies, using coil structures, at microwave frequencies this type of symmetry should be easily obtainable using symmetric windows as the exciting structures.

Reaction Effect

The reaction of the receiver circuit on the exciting current becomes the limiting factor affecting the closeness of spacing between exciter and receiver, when precautions have been taken to reduce the unwanted modes, since exponential decay with spacing implies constant current or voltage excitation, for the TE_{11} and TM_{01} modes respectively. As already mentioned by Harnett and Case,[3] the impedance coupled into the exciter circuit by the receiver decreases as $e^{-2\alpha_{nm}z}$ for the TE_{11} mode, and as $e^{-\alpha_{nm}z}$ for the TM_0 mode, where z is the separation between exciter and receiver. Accordingly, a further advantage of the TE_{11} mode over the TM_{01} mode attenuator is the smaller interaction between exciting and receiving circuits for a given attenuation.

Skin Effect

For a perfectly conducting circular tube, of radius a,

$$\alpha_{11}=\frac{1.841}{a}\left[1-\left(\frac{2\pi}{1.841}\frac{a}{\lambda}\right)^2\right]^{1/2} \quad \text{for the } TE_{11} \text{ mode} \quad (1)$$

$$\alpha_{01}=\frac{2.405}{a}\left[1-\left(\frac{2\pi}{2.405}\frac{a}{\lambda}\right)^2\right]^{1/2} \quad \text{for the } TM_{01} \text{ mode} \quad (2)$$

If one takes into account the effect of finite conductivity, σ, following the method of Stratton,[6] one gets easily, for the attenuation constans,

$$\alpha'_{11}=\frac{1.841}{a}\left[1-\frac{\delta}{a}+\left(\frac{2\pi}{1.841}\frac{a}{\lambda}\right)^2\right]^{1/2} \quad \text{for the } TE_{11} \text{ mode} \quad (3)$$

$$\alpha'_{01}=\frac{2.405}{a}\left[1-\left(\frac{2\pi}{2.405}\frac{a}{\lambda}\right)^2\left(1+\frac{\delta}{a}\right)\right]^{1/2} \quad \text{for the } TM_{01} \text{ mode} \quad (4)$$

where $\delta=\left(\frac{2}{\omega\mu\sigma}\right)^{1/2}$ is the skin depth.

For an i-f attenuator, $a/\lambda<<1$, and it is seen that there is no correction for the TM_{01} mode, whereas the effects of finite conductivity are such as to increase the effective radius from a to $a+(\delta/2)$, for the TE_{11} mode.

Figure 3. Buffing block diagram for measuring crystal nonlinearity

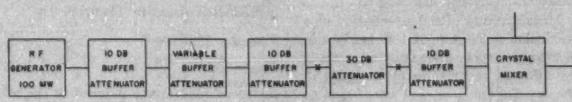


Table I

Rectangular Wave Guide					Circular Wave Guide	
Mode	$\frac{\alpha}{\alpha TE_{11}}$	$\frac{\alpha}{\alpha TE_{21}}$	$\frac{\alpha}{\alpha TE_{31}}$	$\frac{\alpha}{\alpha TE_{41}}$	$\frac{\alpha}{\alpha TE_{11}}$	Mode
	a = b	a = 0.9b	a = b/2	a = b/3		
TE_{11}	1.0	1.0	1.0	1.0	1.0	TE_{11}
TE_{21}	2.0	2.0	2.0	2.0	1.31	TM_{01}
TE_{31}	3.0	3.0	3.0	3.0	1.66	TE_{21}
TE_{01}	1.0	1.1	2.0	3.0	2.08	TE_{01}, TM_{11}
TE_{41}	3.0	2.2	4.0	6.0	2.79	TM_{11}
TE_{11}, TM_{11}	1.41	1.5	2.2	3.1	2.90	TE_{31}
TE_{21}, TM_{21}	2.2	2.4	4.1	6.1	3.0	TM_{21}

Experimental Evaluation of Cutoff Attenuator

Mechanical Description

A circular waveguide-below-cutoff attenuator was built for operation at 20 megacycles per second, with the TE_{11} mode having an attenuation rate of 21 db per inch. The distance between coils is adjusted by a mechanical screw drive, and a mechanical counter reads the attenuation directly in units of 0.01 db. Rectangular coils mounted on precision-made bakelite forms are used in an effort to secure symmetry in the generation of the TE_{11} mode, and hence reduce the unwanted modes. The mechanical precision of construction is such that the accuracy of attenuation reading is ± 0.01 db per inch.

Early experiments with the attenuator revealed that it could not be set to a given attenuation value within plus or minus 0.1 db. The fingers on the moving plunger, as shown in Figure 6, were replaced by helical spring fingers, as described by Sydoriak[2] but with no improvement. Finally, the contacts were removed entirely, and the plunger which supported the coil was constructed of bakelite, eliminating the variable attenuation value for a given setting.

Linearity of Attenuator

Direct Measurement of Attenuator Linearity. In order to check the linearity of the attenuator, that is, the deviation of its rate of attenuation from $e^{-\alpha_{11}z}$, the measurement setup illustrated in Figure 5 was used. The exciting coil in Figure 6 was connected to the i-f signal generator, and the receiving coil was matched to 50 ohms by inserting a resistor and capacitor in series with the coil, and tuning for resonance.

Measurements of the attenuation of a particular 5-db step of the Leeds and Northrup carbon attenuator were made for different separations of the coils in the cutoff attenuator, and were found to be constant within experimental error (plus or minus 0.01 db), for coil separations greater than 20 db (about 1 inch). For smaller separations, the measured values of attenuation were variable, depending upon the angle between the axes of the coils. This departure from linearity was caused partly by the change in exciting current due to coupled impedance of the secondary coil, and partly by the presence of higher order modes.

Measurement of Relative Amplitudes of Higher Modes. The first few higher modes, the TM_{01}, TE_{21}, TM_{11}, and TE_{01}, have attenuation rates of 28, 35, 44, and 44 db per inch, respectively, in the experimental cutoff attenuator. For large enough coil separations the preponderant higher mode will be the TM_{01}, since its attenuation rate is not so very different from that of the TE_{11}. For this condition, a method of measuring the relative amplitudes of the TE_{11} and TM_{01} modes by utilizing their different angular dependence was described by Sydoriak,[2] and developed by Griesheimer.[5] This method assumes that

1. Only the TM_{01} and TE_{11} modes are present in sufficient strength to be of importance.
2. Transfer impedance effects are negligible.
3. The tube cross section has no elliptical eccentricity.

It follows from waveguide theory that the voltage induced in the receiving coil from the TE_{11} mode varies as the cosine of the angle, θ, between the axes of the coils, and that induced from the TE_{01} mode is independent of angle. Then, let

$$V_{TE} = A_1 e^{-\alpha_{11} z} \cos \theta \tag{5}$$

$$V_{TM} = A_2 e^{-\alpha_{01} z} \tag{6}$$

represent the voltage induced in the receiving coil by the TE_{11} and TM_{01} modes, respectively, where z represents the distance between the coils.

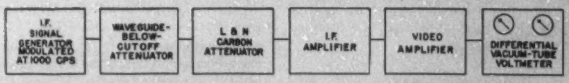


Figure 5. Intermediate-frequency measurement setup for comparing wave-guide-below-cutoff attenuator with Leeds and Northrup carbon attenuator

Since these voltages are separated in time phase by an angle ζ which is approximately 180 degrees, the resultant voltage amplitude is given by

$$V^2 = V_{TE}^2 + V_{TM}^2 + 2V_{TE}V_{TM} \cos \zeta \tag{7}$$

This may also be written

$$V^2 = (A_1 e^{-\alpha_{11} z})^2 [1 + r^2 e^{-2bz} + 2re^{-bz} \cos \theta' \cos \zeta - \sin^2 \theta] \tag{8}$$

where

$$r = \frac{A_2}{A_1} \tag{9}$$

and

$$b = \alpha_{01} - \alpha_{11} \tag{10}$$

If we keep z constant, and vary θ, V^2 will have a maximum, V^2_{max}, at $\theta = \pm\pi$, and a secondary maximum, V^2_{submax} at $\theta = 0$, and a minimum, V^2_{min}, at $\cos \theta' = -re^{-bz} \cos \zeta$. If we assume the coils are adjusted for maximum power transfer, $\theta = \pi$, then it follows from equation 8 that the attenuation in decibels introduced by displacing the secondary coil from an initial separation, z_0, between coils to a final separation z, is given by

$$A_{db} = 10 \log_{10} \exp(-2\alpha_{11}[z_0 - z]) + 10 \log_{10} \frac{1 + r^2 \exp(-2bz_0) - 2r \exp(-bz_0) \cos \zeta}{1 + r^2 \exp(-2bz) - 2r \exp(-bz) \cos \zeta} \tag{11}$$

From equation 11, it is seen that the presence of the TM_{01} mode introduces a deviation from the exponential law of decay which obtains when only the TE_{11} mode is present. If we define this deviation of the actual attenuation in decibels from the theoretical exponential value as the error, E_{db}, due to the TM_{01} mode, it follows from equation 11 that the maximum error in a measurement of attenuation where the minimum separation between coils is z_0, is

$$E_{db} = 10 \log_{10} [1 + r^2 \exp(-2bz_0) - 2r \exp(-bz_0) \cos \zeta] \tag{12}$$

The value of E_{db} for a given minimum

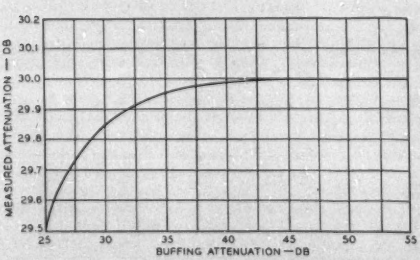


Figure 4. Measured attenuation of nominal 30-db radio-frequency attenuator as a function of total buffing attenuation using 100 milliwatts of input power

A buffing attenuation of 40-db corresponds to a crystal input power of 10 microwatts

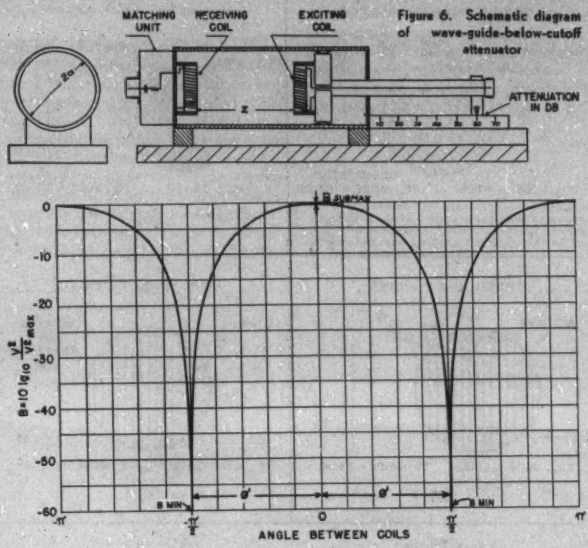


Figure 6. Schematic diagram of wave-guide-below-cutoff attenuator

Figure 7. Graph of $B = 10 \log (V^2/V^2_{max}$ as a function of angular rotation of coils for TE_{11} mode circular attenuator

separation between coils may be determined experimentally by measuring

$$\frac{V^2_{submax}}{V^2_{max}}$$

and

$$\frac{V^2_{min}}{V^2_{max}}$$

at the coil separation z_0.

It is convenient to define the following quantities:

$$B = 10 \log_{10} \frac{V^2}{V^2_{max}} \tag{13}$$

$$B_{submax} = 10 \log \frac{V^2_{submax}}{V^2_{max}} \tag{14}$$

$$B_{min} = 10 \log_{10} \frac{V^2_{min}}{V^2_{max}} \tag{15}$$

$$\frac{V^2_{submax}}{V^2_{max}} = h^2$$

$$\frac{V^2_{min}}{V^2_{max}} = \beta^2 \tag{16}$$

Figure 7 is a typical plot of B as a function of angle between the axes of the coils, and illustrates the power coupled into the receiving coil as a function of angle of rotation, between the coils. If the two values of B_{min} in Figure 7 are not the same, then a third mode must be present. Also if the minimum points are not displaced by equal angles to either side of B_{submax}, a third mode must be present. Using the measured values of B_{submax} and B_{min} as defined herein, the values for r and ζ may be determined from the following equations, as derived by Griesheimer.[6]

$$J = \left[\frac{h^2 - \beta^2}{1 - \beta^2}\right]^{1/2} \tag{17}$$

$$re^{-bz_0} = \frac{1-J}{1+J}\left[1 + \frac{4\beta^2}{1-h^2}\frac{(1+J)}{(1-J)}\right]^{1/2} \tag{18}$$

$$\cos \zeta = -\left[1 + \frac{4\beta^2(1+J)}{1-h^2(1-J)}\right]^{-1/2} \tag{19}$$

$$\cos \theta' = \frac{(1-J)}{(1+J)} \tag{20}$$

Measurements on the experimental TE_{11} mode cutoff attenuator, using rectangular coils, showed that the angular calibration was symmetrical within the accuracy of the measurement, and it was concluded that there was no appreciable coupling due to modes higher than the TM_{01}. The value of B_{submax} was found to be -0.30, and the value of B_{min} to be -60. From equations 16–20 the following values of r, ζ, and θ' were obtained $re^{-bz_0} = 0.017$, $\zeta = 176.5$ degrees, and $\theta' = 89$ degrees.

The maximum error in attenuation, E_{db}, for this separation was therefore, from equation 12, 0.14 db. For a minimum coil separation corresponding to an attenuation of 20 db, the maximum error

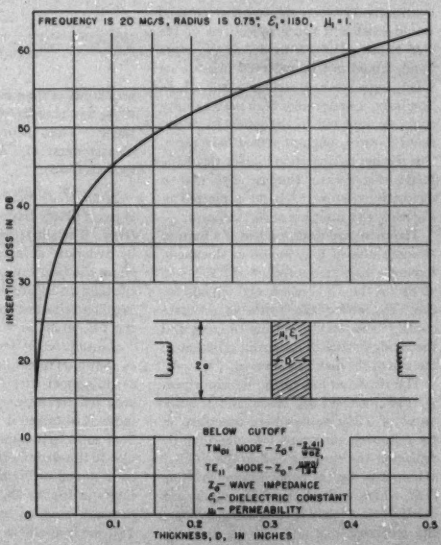


Figure 8. Calculated insertion loss of barium titanate disk for TM_{01} mode versus thickness of disk

in attenuation was 0.08 db. The latter error was not detected by measuring the 5-db step of the Leeds and Northrup attenuator because the error was spread over such a wide range of attenuation that the amount included in a 5-db interval was smaller than the experimental error.

Dielectric Filters. The equation for the TM-mode wave impedance, Z_{nm}, of a circular hollow tube of radius a, containing a medium of permeability, μ_1, and dielectric constant of ξ_1, is given by

$$Z_{nm}=\left[\left(1-\frac{f_{nm}^2}{f^2}\right)\left(\frac{\mu_1}{\xi_1}\right)\right]^{1/2} \tag{21}$$

This simplifies, at frequencies far below cutoff, to

$$Z_{nm}=\frac{-U_{nm}j}{2\pi f a \xi_1} \tag{22}$$

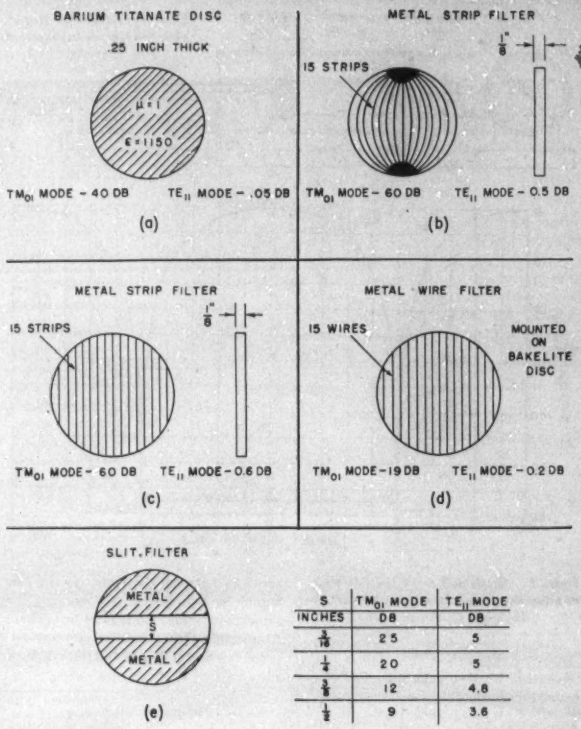
For TE waves, the wave impedance simplifies to:

$$Z_{nm}=\frac{a\mu_1 2\pi f j}{v_{nm}} \tag{23}$$

Here, f_{nm} and f are the cutoff and impressed frequencies, $j=\sqrt{-1}$, and U_{nm} and v_{nm} are numerical factors which depend upon the mode. Hence, a material of high dielectric constant will reflect the TM wave and pass unchanged the TE wave. The high dielectric constant material has a wave impedance, for the TM mode, inversely proportional to the dielectric constant so that a large discontinuity between air and the material would exist and cause reflections of the TM mode. The TE mode, on the other hand, would not be reflected because its wave impedance is independent of ξ_1. Similarly, a material of high permeability could be inserted in the guide to reflect the TE wave, but not reflect TM wave. The theoretical insertion loss for the TM_{01} mode of a barium titanate disk with a dielectric constant of 1,150 is plotted in Figure 8, as a function of its thickness.

The measured insertion loss of a barium titanate disk of 0.25 inches in thickness, having a dielectric constant of 1,150, was 40 db for the TM_{01} mode, and 0.05 db for the TE_{11} mode. To insure an intimate contact between the barium titanate and the inside surface of the cutoff attenuator, the rim of the disk was silvered.

The insertion loss of the barium titanate disk for the TM_{01} mode, and of other types of TM_{01} mode filters described in the following paragraph was measured by replacing the exciting coil in the circular attenuator by a capacitative exciting disk, which excited the TM_{01} mode primarily. At a fairly close spacing between the receiving coil and exciting disk, the amplitude of the TE_{01} mode was measured to be less than 1/1,000 that of the TM_{01} mode. Thus there was ample range for measurement of TM_{01} insertion loss for various filters.



S INCHES	TM_{01} MODE DB	TE_{11} MODE DB
$\frac{3}{16}$	25	5
$\frac{1}{4}$	20	5
$\frac{3}{8}$	12	4.8
$\frac{1}{2}$	9	3.6

Figure 9. Insertion loss of various mode filters for TE_{11} and TM_{01} modes in cutoff attenuator

Metal Mode Filters. Various other types of filters illustrated in Figure 9 were tried. The slit filter, Figure 9e, was used by Sydoriak[2] at higher frequencies, but it is not the best type for i-f frequencies, as the data on Figure 9 indicate, because the slit filter reflects a considerable amount of the TE_{11} mode as well as the TM_{01} mode.

Gainsborough[1] developed the wire filter as shown in Figure 9d. It is not as effective as the copper-strip filter shown in Figure 9c. A little less insertion loss for the TE_{11} mode is obtained if the copper strips are bent so as to be approximately perpendicular to the electric lines of the TE_{11} mode as is shown in Figure 9b. Of course the filters in Figure 9b, c, d, and e must be properly oriented or they will reflect the TE_{11} mode as well as the TM_{01} mode.

Using the metal strip filter of Figure 9b an angular calibration of the cutoff attenuator was made. The value of B_{submax} was zero within limits of measurement, and B_{min} was less than -80.

Comparison With Leeds and Northrup Carbon Attenuator. A calibration of the Leeds and Northrup carbon attenuator was made over a 40-db range in attenuation at 20 megacycles per record. The resulting attenuation calibration checked within 0.03 db the calibration made with direct current. A check was then made by operating the cutoff attenuator as a TM_{01} attenuator as described previously for TM_{01} mode-filter insertion loss. The TM_{01} mode calibration was made at only two values of attenuation, 10 db and 20 db, and the values obtained checked the previous calibration within plus or minus 0.01 db.

Conclusion

The heterodyne or i-f substitution method of calibrating microwave attenuators has been investigated, and preliminary equipment has been built to utilize the method. The accuracy obtained is estimated to be within plus or minus 0.02 db in the 0–10 db attenuation range, and plus or minus 0.2 per cent of the attenuation value in db for the 10–50 db range, assuming unity standing-wave ratio presented to both sides of the attenuator under calibration. The basic standard of the heterodyne method is a waveguide-below-cutoff attenuator operated at an intermediate frequency. A preliminary model of an i-f standard cutoff attenuator has been built, tested, and improved until its accuracy is plus or minus 0.01 db per 20 db unit of attenuation.

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No Discussion

Speed-Responsive Devices on Diesel-Electric Locomotives

A. V. JOHANSSON
ASSOCIATE AIEE

THIS PAPER describes a control system used on large diesel-electric locomotives to initiate changes in motor connections at certain train speeds. Figure 1 shows a modern locomotive on which this control system is used.

Most large diesel-electric locomotives employed in freight and passenger service on domestic railroads have two motor combinations, each with one or more steps of motor-field shunting, in order to provide full utilization of engine horsepower over a wide range of train speed while meeting the requirement that the electric equipment be as light in weight as possible, consistent with good performance. High schedule speeds require that within the limits of adhesion, available engine horsepower be delivered to the wheels at all times during acceleration. To accomplish this, it is necessary to change to weak field and to new motor connections at the proper times during the cycle of acceleration. This has been done on freight locomotives by manual selection, with the operator reading a speed indicator and changing his connections at certain train speeds. In passenger service, with its higher acceleration rates and greater schedule speeds, and therefore greater dependence on maintenance of full utilization of engine horsepower during acceleration, it has been found desirable to provide automatic selection of motor connections in order to relieve the operator of this responsibility and to maintain more uniform acceleration of the train than would be had by manual selection.

Several means of automatic selection have been used, the simplest using a voltage relay which operates when the generator voltage reaches a certain point. Another method uses a relay which operates on generator voltage and current, and this method is superior to the first in that it will operate at any engine speed rather than at the top speed only. These methods have been used on older locomotives which did not have field shunting in both motor combinations. When one or more steps of field shunting in each of two motor combinations are required, the number of such relays becomes large, the circuits are rather involved, and transients during switching present a difficult problem. This is especially true when automatic backward transition and unshunting are required.

A new means of automatically selecting motor connections in several steps, either forward with increasing speed or back-

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A. V. JOHANSSON is a member of the control engineering division, General Electric Company, Erie, Pa.

Figure 1. A modern Diesel-electric locomotive

Figure 2. Axle generator, before mounting on locomotive

Figure 3. Axle generator mounted on locomotive

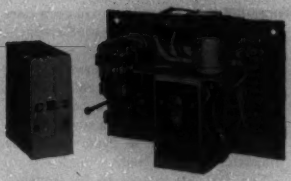


Figure 4. Speed-sensitive relay panel, front view

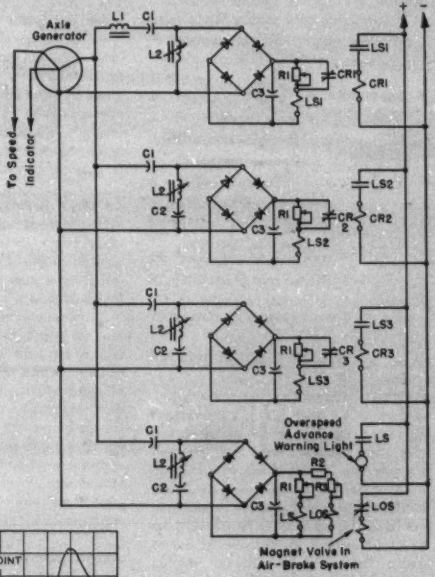
Figure 5. Speed-sensitive relay panel, rear view

ward as the locomotive decelerates during operation up a heavy grade, has been devised and applied to new diesel-electric passenger locomotives, electric equipment for which is built by the General Electric Company. This method utilizes a group of relays energized through the medium of a tuned circuit by an a-c generator mounted on a locomotive axle. Because of the tuned circuit, the voltage across the relay coil increases very rapidly as the speed at which the relay is to operate is approached, providing an inherently accurate and stable speed setting of the relay and a drop-out speed close to the pick-up value.

The generator used has a 14-pole permanent-magnet rotor, the output being taken from the stator coils, which are 3-phase Y-connected, with the neutral brought out. The generator is bolted to a special cover on the antifriction journal box on the end of one of the locomotive axles, replacing the journal box cover. The rotor is driven by the locomotive axle through a double-end splined drive shaft. This shaft is so designed that it may be disengaged from the axle, pulled through the rotor, and engaged with a motor-driven calibrating mechanism in order that the generator may be conveniently operated for relay calibration without removing it from the locomotive. Figures 2 and 3 show the generator before and after mounting.

The generator energizes the speed relays and, in addition, the locomotive speed indicator. Each speed-relay panel consists of one or more reactors, suitable capacitors, a full-wave rectifier, a small hermetically-sealed sensitive relay, and a multiple-contact control relay. The control relay, energized by 75-volt d-c locomotive control power fed through the contacts of the sensitive relay, controls field shunting contactors or starts the transition sequence leading to a new motor combination, as the case may be. In addition to the three relay panels required for the two connections of field shunting and the transition, a fourth relay panel may be used to provide a warning of impending locomotive overspeed and to shut off power and apply air brakes in the event of actual locomotive overspeed. One of the speed relay panels before assembly in a locomotive is shown in Figures 4 and 5. A schematic connection diagram of the speed relays and generator on a locomotive is shown in Figure 6. As will be noted from the diagram, the speed indicator is connected between one phase and the neutral of the generator and is therefore not materially affected by the loading presented by the speed relays. The speed relays are connected across two phases of the generator, and operate from the algebraic sum of the voltages produced in each of these phases.

Figure 6. Schematic diagram of relay circuits



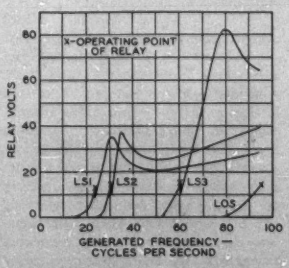


Figure 7. Curves showing relay coil voltages plotted against frequency of generator output

In Figure 6 the speed-relay circuits are shown from top to bottom in the order in which they operate, with the one operating at the lowest speed shown at the top. As shown in the diagram, the circuits are alike between the generator and full-wave rectifier except for the circuit operating relay *LS1*. Referring to the curve for this circuit, shown in Figure 7, it will be noted that resonance occurs at 31 cycles

per second. Reactor *L-1* in series with the load is used to hold the output voltage down while above the resonant frequency, as the generator voltage is proportional to speed, and the relay coil current would otherwise rise above a safe continuous value. Since the curves for relays *LS1* and *LS2* are similar in shape and close together in frequency, though the circuits differ, it should be pointed out that each circuit is also used on locomotives requiring other settings, and while the ranges of possible settings for each circuit and set of components overlap slightly, the circuit for *LS2* does not cover enough range to meet the requirements for *LS1*.

As shown by the curves, the relays pick up at a rather low voltage compared with the maximum reached, and this assures the relays' holding in when the droop in voltage occurs at speeds above the resonant point. It will be noted that the relays pick up and drop out on a very steep part of the voltage curve, providing an inherently stable setting and a drop out value close to the pickup value. Where required, the drop out value is raised even closer to the pickup value by means of the insertion of resistance in the coil circuit of the sensitive relay by the control relay operated by the speed relay in each case.

The construction of a speed-relay equipment panel is shown in Figures 4 and 5. The control relay has a separate cover to prevent the entrance of dirt and the sensitive relay is enclosed in a hermetically sealed can. Since the remainder of the equipment is static in type, very little maintenance is required.

The first locomotive to be equipped with speed-sensitive relays went into service during the last year and many others are now in use. Results have been very gratifying both as to stability of settings and reliability of operation.

No Discussion

Electric Locomotives With Identical Basic Components

W. A. BRECHT
ASSOCIATE AIEE

CHARLES KERR, JR.
NONMEMBER AIEE

Synopsis: A series of electric locomotives of varying capacities can be constructed from standardized components suitable for switcher, freight and passenger services. Superior operating characteristics are attained while reducing first and operating costs as compared with existing motive power.

ELECTRIFICATION is widely acknowledged to be the best means yet devised for the operation of a railroad. By its use, a standard of operation can be maintained which is unobtainable with other types of power. This superior service generally is accompanied by a reduction in direct operating expense. The predominant factor which has restricted the more widespread use of electrification is the large initial investment required.

An analysis of many electrifications shows that the locomotives themselves constitute 30 to 40 per cent of the total investment. While it is necessary to reduce the cost of all parts of the electrification system, locomotives play such an important role in the over-all operation of a railroad that a complete survey of the modern electric locomotive is well justified at this time from the viewpoint of its over-all operating characteristics as well as first cost.

Locomotive Requirements

A locomotive should meet certain requirements to be a thoroughly satisfactory vehicle for the movement of present-day rail traffic.

High availability must be provided because low operating costs can only be secured by intensive utilization of the motive power. Without high availability low fixed charges and low maintenance are impossible of attainment. The over-all design must provide a locomotive which will have a minimum of road failures and one which can be easily and cheaply maintained. Motive power has been getting more expensive continually, and more and more attention must be given to the problem of keeping it running.

As train speeds are generally being increased it is essential that the locomotive be a good tracking vehicle. Good tracking characteristics and moderate axle loadings considerably reduce track maintenance and also reduce maintenance on equipment carried on the locomotive.

Rigid standardization is one of the best means for minimizing first costs and maintenance. If standardization is to be carried to its ultimate, many parts of locomotives should be duplicated for both freight and passenger service. The difference in speed between the two is decreasing, and in many instances power is operated interchangeably. Consequently the trend is toward less and less distinction in the construction of locomotives for the two types of service.

In recent years a very material change has come about in the conception of the ideal speed—tractive effort characteristics of a locomotive. This has been brought about largely by the success of the Diesel. Sustained horsepower output is still desirable over the whole speed range. But in addition to this, the advantages of large starting tractive effort and high continuous tractive effort are appreciated more and more, and the economies to be derived from their provision are better recognized. This means that in the design of a locomotive it is desirable to carry, if possible, all of the weight on driving axles. As a means to reduce track maintenance it is preferable to have a large number of lightly loaded axles than a small number of heavily loaded axles. The optimum adhesive weight for a locomotive seems to be about 100 pounds for each rail horsepower. Locomotives built with this weight ratio provide easy handling of trains, give maximum tonnage rating over adverse grades, and reduce helper service.

Summarizing, the requirements for a modern locomotive are

1. High availability.
2. Standardization and low first cost.
3. Low operating and maintenance costs.
4. Good tracking qualities.
5. Light axle loads.
6. Sustained output over the entire speed range.
7. Large starting tractive effort with entire locomotive weight on drivers.
8. High continuous tractive effort.
9. An adhesive weight of approximately 100 pounds per continuous rail horsepower.

Existing Motive Power

No locomotive has altogether met these requirements in the past. The reciprocating steam locomotive has probably departed most from them and this is perhaps one of the main reasons why its popularity has suffered. For the most part a small portion of its weight is carried on drivers with ratios of weight on drivers to rail horsepower as low as 45 to 60 pounds except for articulated types. Even this low ratio is often achieved only by using excessive axle loads, the destructive effect of which on the track is further increased by the dynamic augment of the reciprocating parts.

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W. A. Brecht is manager of transportation engineering and Charles Kerr, Jr., consulting transportation engineer, Westinghouse Electric Corporation, East Pittsburgh, Pa.

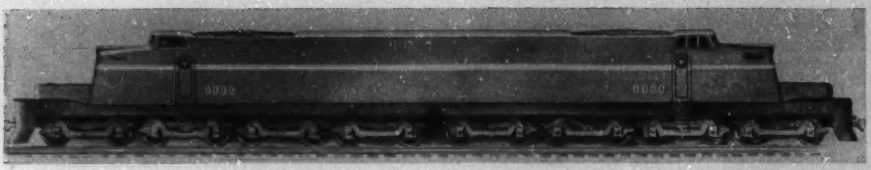


Figure 1. Artist's conception of 7,500-horsepower locomotive

Electric locomotives in the past have met a number of these requirements, but have departed from them in a lack of standardization and in the tendency to concentrate great blocks of power on each driving axle, resulting in insufficient weight—horsepower ratios and inherently slippery motive power with little reserve starting capacity.

The Diesel does not fully meet all of them but it probably comes closer than any other kind now in operation. Inherently a diesel locomotive is heavy and long, introducing factors which result in a high first cost and increased operating expense.

From the maintenance standpoint, steam reciprocating locomotives are most costly, reciprocating Diesel engines next, while the electric locomotive, which carries no prime mover, is the most favorable.

As the electric locomotive does not carry its own prime mover or fuel, it inherently has a greater possibility of meeting all requirements of ideal motive power than any of its competitors. In addition, electrification has a very decided advantage in its sources of energy which are coal, efficiently burned in large central stations, or water power. These constitute the nation's largest and most assured sources of low cost energy. Furthermore, when atomic power becomes a practical source of low cost energy it will be produced initially in large central stations, and will be available to the railroads only if they are electrified.

Proposed Electric Locomotives

The ideal electric locomotive should meet the basic requirements which have been set forth. In addition to those listed, it should be capable of being built in one cab for any capacity required. The general arrangement of the locomotive should permit installation of electrical apparatus to operate from either alternating or direct current. Both systems are widely used throughout the world and both provide excellent means of operation. The locomotives proposed here are of the straight a-c or d-c types which are fundamentally suited for the widest range of applications. The motor-generator and split-phase types show to best advantage only on mountain grades, are more expensive than the types selected, and less flexible in their application.

It is proposed to build, from standardized mechanical and electrical components, a series of locomotives of varying capacities to fill every motive power need in the wide range of railroad application. The first component is a simple four-wheel swivel truck designed to carry two axle-hung traction motors which may be of the a-c or d-c type. This truck, with its motors, will constitute the running gear and main propulsion units which normally require the major part of locomotive maintenance. Consequently, by making all motors and trucks identical, the major part of standardization is accomplished insofar as wearing parts are concerned. A small stock of spare parts can then service a variety of locomotives ranging from switchers to large road units.

In current Diesel practice, the largest concentration of power on a single driving axle is 500 engine horsepower or 400 rail horsepower. For the standardized electric locomotive truck, 469 rail horsepower has been chosen for the capacity of each traction motor, a value that can be met with d-c or a-c motors. By utilizing different numbers of these trucks the following series of single cab locomotives is obtained.

Trucks	Rail Horsepower Continuous	Rail Horsepower Maximum
2	1,875	2,500
3	2,813	3,750
4	3,750	5,000
6	5,625	7,500
8	7,500	10,000

In this series of locomotives, each axle will be loaded to only 45,000 pounds so all will have unusually low wheel loadings, resulting in low maintenance of wheels, track, and bridges, and meeting all main line weight restrictions. This range of sizes should cover any applications now existent or which are predictable in the near future. The largest diesel locomotives currently in service deliver approximately 5,000 rail horsepower while the largest steam engines range between 6,000 and 7,000 rail horsepower. In comparing ratings of different locomotives, it should be kept in mind that diesel and steam horsepower capacities are maximum ratings, while the electric locomotives are continuously rated and can be operated in excess of these ratings for short periods.

Mechanical Arrangement

The proposed standard four-wheel truck will be of special, but not radical, design. It will be a "no-lift" lateral motion truck in which the lateral swing of the bolster is restrained by metallic or rubber springs or, by their omission, allowed free lateral motion. Where more than two trucks carry a single cab, the two outer trucks will have their lateral motion restrained to produce lateral characteristics similar to those of conventional swing link trucks except that there will be no change in the height of the truck centerplate above the rail with lateral motion. The trucks between any two outer trucks will be unrestrained laterally. To further minimize truck-to-truck weight transfer with this arrangement because of vertical curvature or irregularities of the track, the truck is provided with unusually flexible springs having static deflections of the order of $5^1/_2$ inches. With these springs maximum truck-to-truck weight transfer will be held under all conditions to less than 5 per cent.

Normal clearance restrictions can be met with any combination involving up to 16 axles under a single cab with this arrangement. As a matter of fact, a 16 axle, 7,500 horsepower locomotive shown in Figure 1, while approximately 117 feet long between coupler faces and having a cab 90 feet long, meets all normal clearance restrictions on any flat or superelevated curve of 300 feet radius or more with lateral displacements no greater at any point than those of a standard 85 foot passenger car.

The cab will be carried directly on the trucks on two, three or four truck locomotives. The construction used is shown

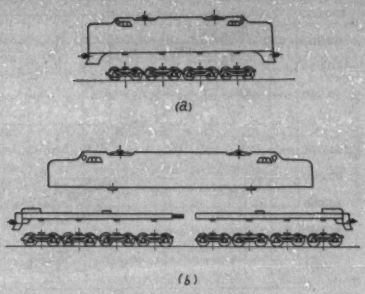
schematically by the exploded view in Figure 2*a*. When six or eight trucks are required, they will be divided into two groups, each assembled under a subframe with the two frames in turn carrying the locomotive cab, as shown in Figure 2*b*. These combinations are suitable for speeds up to 100 miles per hour with excellent tracking characteristics, and may readily be made to track well at still higher speeds by adding simple damping devices to the trucks.

It should be noted that these locomotives, in the larger sizes, are only about 45 per cent of the length of Diesel locomotives of equivalent rail horsepower. This type of mechanical arrangement would not permit Diesel locomotives to be built to these same lengths because of the space required for the Diesel engines and their auxiliaries.

Figure 2.

(a). Exploded view of 4-truck, 3,750 horsepower locomotive

(b). Exploded view of 8- truck, 7,500 horsepower locomotive



Electric Equipment

The electric apparatus can be highly standardized to cover the five sizes of locomotives ranging from 1,875 to 7,500 horsepower.

When built for operation under an a-c trolley, one design of traction motor can be used for all services; switching, freight or passenger, the proper gear ratio being selected for each service. Three sizes of transformers will cover the entire range of capacities with two transformers being used in each of the two larger sizes. Most control components will be identical, including such items as unit switches, reversers, master controllers and train line jumpers. Different quantities will be required on different sizes. The same traction motor blowers will be used, each blower supplying four motors. Compact force-ventilated resistor units can be added to provide dynamic braking in freight service when it is desired. This space will be used for train heating boilers for passenger service. All cab apparatus will be grouped in subassemblies which can be readily removed for repair.

When built for d-c operation a single design of traction motor with proper gearing will be used for all services. Traction motor blowers will be identical as will the component parts of the control, varying in quantity only for the different sizes. In other words the same degree of standardization can be carried out whether alternating or direct current.

The electric equipment was selected to provide the fundamental propulsion characteristics required for the chosen axle loading. Generally speaking the most severe duty on the electric equipment occurs in freight service. The motor capacity was selected to give a continuous tractive effort equivalent to $16^3/_4$ per cent adhesion with freight gearing. It has been found from experience that freight locomotives with this adhesion rating are normally self protecting and can be universally applied. Sufficient starting tractive effort has been provided to work the motors at a conservative commutating limit at 25 per cent adhesion, with sufficient margin for emergency starting at 35 per cent adhesion. With these margins the motors can be re-geared for switcher or passenger service.

For some passenger applications it may be necessary to provide a truck, identical with the others but without traction motors, to carry the additional weight of the train heating boiler and its supplies.

The continuous rating of 469 horsepower per motor is ideal for the locomotive weights proposed. As one of the inherent characteristics of electric traction motors is their short time overload capacity, all of these locomotives can exert outputs in excess of their continuous rating for short periods. A satisfactory limit to overload capacity is $33^1/_3$ per cent in excess of the nominal continuous rating, and the output of all locomotives will be automatically limited to this value. By so doing, the maintenance of equipment will be kept low and advantages gained in the construction of the power supply system.

Table I. Weights, Dimensions and Ratings Electric Locomotives—1875 to 7500 Horsepower with Identical Motors and Trucks

	1875 Horsepower	2813 Horsepower	3750 Horsepower	5625 Horsepower	7500 Horsepower
Arrangement					
Total weight—lb	180,000	270,000	360,000	540,000	720,000
On drivers—lb	180,000	270,000	360,000	540,000	720,000
Per driving axle—lb	45,000	45,000	45,000	45,000	45,000
Length over couplers	48′	67′	70′ 4″	99′ 10″	117′ 2″
Length cab	44′	64′	87′	88′	90′
Rigid wheel base	8′ 4″	8′ 4″	8′ 4″	8′ 4″	8′ 4″
Wheel diameter—inches	44″	44″	44″	44″	44″
Number of motors	4	6	8	12	16
Ratings—freight gearing					
Tractive effort—25% adhesion	45,000	67,500	90,000	135,000	180,000
Maximum tractive effort—35% adhesion	62,500	93,750	125,000	187,500	250,000
Maximum horsepower	2,500	3,750	5,000	7,500	10,000
Continuous ratings—$32^1/_3$ mph					
Horsepower	1,875	2,813	3,750	5,625	7,500
Tractive effort	21,625	32,437	43,250	64,875	86,500
Maximum continuous tractive effort	30,000	45,000	60,000	90,000	120,000
Maximum speed—mph	65	65	65	65	65

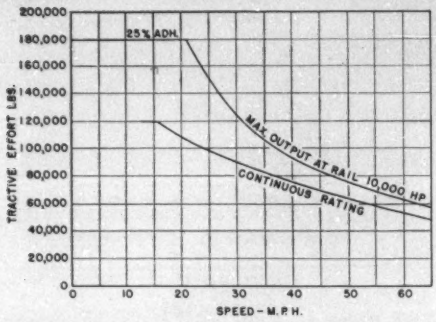


Figure 3. Performance curve of 7,500-horsepower freight locomotive

Weights, Dimensions, and Ratings

Table I shows the principal weights, dimensions and ratings of the proposed line of locomotives ranging from 1,875 to 7,500 horsepower. In this tabulation the ratings are listed for locomotives equipped with gearing suitable for freight service. If gearing for other services at different top speeds were selected, the speeds and tractive efforts would change accordingly.

One particularly interesting feature of this line of locomotives is the constant weight ratio of approximately 100 pounds per continuous rail horsepower maintained throughout the entire line. Another advantage is the ability to operate any or all combinations in multiple so that if in any particular service it is desirable to operate a two-unit locomotive rather than one big unit, this is entirely practical.

Figure 3 shows the performance curve of the 16 axle locomotive, rating 7,500 horsepower continuously. This locomotive, in a single cab, is capable of delivering 120,000-pounds tractive effort continuously, and a maximum rating of 10,000 horsepower at all speeds between 20 and 65 miles per hour. One electric cab provides approximately the continuous tractive effort of the average 4-unit, 6,000 horsepower Diesel, and twice its maximum rail horsepower.

It has been customary throughout the industry to rate locomotives by horsepower but unfortunately all of these horsepower ratings are not comparable. Steam locomotives are normally rated in cylinder horsepower. Diesel locomotives are rated in engine horsepower available for input to the generator for traction. Electric locomotives are rated in continuous horsepower at the rail. To show a comparison between the various types as they are normally rated, Figure 4 shows the speed-tractive effort performance of reciprocating steam, Diesel and electric freight locomotives per 1,000 horsepower of nominal rating.

One other feature of this curve warrants attention. Electric transmissions can be used on Diesel, steam turbine or gas turbine locomotives. Identical electric drives will give exactly the same performance at the rail irrespective of the prime mover used. The tractive effort which can be developed with any electric drive is a function of the number of traction motors used. The limits shown on this curve correspond to the average for Diesels now in service.

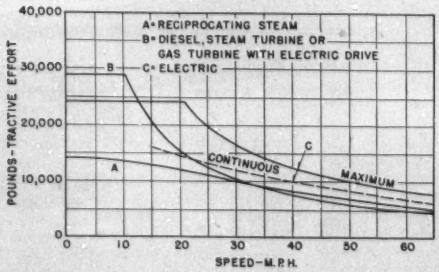


Figure 4. Speed-tractive effort characteristics of steam, Diesel and electric locomotives per 1,000 horsepower of nominal rating

The comparative weights, per rail horsepower, of modern locomotives are shown in the following tabulation.

Type	Total Weight Lb Per Continuous Rail Hp	Adhesive Weight Lb Per Continuous Rail Hp
Proposed electrics	100	100
Reciprocating steam		
(Rigid frame)	140 to 160	45 to 60
(Articulated)	150 to 170	75 to 100
Diesel	155 to 240	135 to 185

Costs

This standardized construction of locomotives, besides making available a series which should meet any predictable operating conditions, will be attractive from the standpoint of low first cost when locomotives are manufactured in quantities comparable to the production of other types. The following tabulation shows the comparative quantity prices of this line of electric locomotives, of modern steam reciprocating locomotives, and of Diesel road locomotives. The Diesel locomotive was selected as the standard of comparison since it is currently being purchased in the greatest quantities.

	Relative Selling Price Per Rail Horsepower (Per Cent)
Diesel	100
Steam reciprocating (rigid frame)	50 to 60
Proposed electrics	
1,875 horsepower	68
7,500 horsepower	58

In appraising this tabulation one other factor should be kept in mind. The electric locomotive inherently has the highest availability of any type and for its horsepower rating will do more work. Consequently, fewer electric locomotives are usually required to do a given job which makes the comparison even more favorable for the electrics than that shown in the table.

On the other hand the electric locomotive requires wayside structures and installations which are necessary for its use. When these are included, the first cost of electrification has generally exceeded that of other types of power. This higher investment can be justified, by the locomotive's ability to do more work at lower operating expense.

It is of equal importance to have locomotives with low operating expense. One of the principle means by which this can be accomplished is with motive power suitable for handling heavy tonnage

trains over any profile with the minimum degree of helper service. Among the proposed locomotives a size can be selected which will fit requirements of any railroad for the expeditious handling of any type of traffic. As all of these locomotives carry their entire weight on drivers, the maximum utilization of horsepower is obtained for every condition required.

A second important item in operating expense is that of fuel or power. The electric locomotive itself is a very efficient machine operating at an over-all efficiency of 80 to 85 per cent from trolley to rail. Actually the expense for power is largely contingent on the price at which it is delivered to the trolley system, a subject beyond the scope of this paper.

The importance of low maintenance cannot be overemphasized. The maintenance expense of some modern locomotives equal their original purchase price in five years. Past performance shows that electric locomotives can be maintained for 1/3 to 1/2 of the cost per locomotive mile of Diesel or reciprocating steam locomotives when comparing locomotives of the same rail horsepower. The proposed line of electric locomotives should offer possibilities of further reduction in maintenance because of their design and standardization which permit the adoption of maintenance methods not heretofore commonly used. It should be possible to eliminate periodical major shoppings entirely. For instance, it will be a very short job to drop out one truck and replace it with one which has, together with its motors, been overhauled. It should be possible to do this without interfering with operating schedules. Similarly all major apparatus within the locomotive cab consists of units which can be removed and replaced without taking the locomotive out of service. Maintenance then becomes a steady shop load which can be handled with a maximum of efficiency so that it should seldom be necessary to remove a locomotive from service to keep it in first-class operating condition.

Conclusion

The ideas discussed in this paper constitute a new conception of electric locomotive design. It is the belief of the authors that they offer a definite basis for the construction of lower cost motive power with improved operating characteristics and reduced operating expense.

Discussion

D. R. MacLeod (General Electric Company, Erie, Pa.): We agree with Brecht and Kerr that it would be very desirable to build a more standardized electric locomotive than we have in the past. We do not agree with the method of standardization that they have suggested. It is just as desirable to standardize locomotive manufacture as it is to standardize the component parts. The type of locomotive described in the paper would have different unit lengths for the different ratings. This is a serious disadvantage from the standpoint of the locomotive builder. For example, a locomotive 117 feet long would require facilities that are not available in a shop equipped to build large quantities of locomotives that are half the length.

The details of design are not described and detailed analysis and layouts would have to be made to evaluate the proposed design. The general description of the mechanical arrangement appears to be quite feasible. Our experience in building locomotives with span bolsters has revealed many disadvantages of this type of construction that are not readily apparent. It is the writer's opinion that the 5,625 and 7,500 horsepower locomotives would be a better over-all job if built in two cabs of the smaller units.

The statement is made that one traction motor can be used with 45,000 pounds per axle with the different gearing required for switching, freight or passenger service. This is very interesting and deserves further explanation. Alternating current series motors of the type in general use today are applied on the basis of a certain volts per bar when starting at 25 per cent adhesion. With the same weight on the drivers in all cases, the volts per bar when starting at 25 per cent adhesion will be approximately in proportion to the maximum speeds for which the locomotives are geared.

The authors state that the optimum adhesive weight ratio for an electric locomotive appears to be about 100 pounds for each rail horsepower. We do not believe that there is any single weight ratio that will fit all applications. During the last twelve months the General Electric Company has completed the largest single-cab electric locomotives and four electric locomotives with the greatest adhesive weight that have been built to date. These locomotives had an adhesive weight of approximately 150 pounds per rail horsepower. Higher horsepower in relation to weight on drivers was not required. On the other hand, there are applications of high-speed passenger locomotives where an adhesive weight of less than 100 pounds can be used to advantage. The optimum weight ratio for a given application is the ratio of the adhesive weight that is required to negotiate the heaviest grade divided by the maximum horsepower that can be used to advantage anywhere on the profile. A Diesel-electric locomotive designed for freight service has a high continuous tractive effort rating which enables it to haul heavy trains over heavy grades. A 4,500 horsepower Alco-GE Diesel-electric locomotive has a higher continuous tractive effort than the 7,500 horsepower (at the rail) locomotive illustrated by Figure 3 of the paper. It can haul the same tons as the 7,500 horsepower locomotive over a ruling grade but must do it at a slower speed. The higher the speed that is to be maintained over the entire profile the higher is the horsepower required and the lower is the weight ratio in pounds per horsepower that is desirable.

Any standardization that is adopted for electric locomotives should be capable of sufficient variations so that their inherent flexibility is not handicapped.

C. K. Steins (The Pennsylvania Railroad, Philadelphia, Pa.): Brecht and Kerr have presented a very interesting conception of the electric locomotive of the future. They propose to standardize locomotives below the cab underframe and perhaps it is planned to carry this standardization into the cab itself, although their necessarily brief paper does not bring this out clearly.

It should be possible in this locomotive to house the cab apparatus for, say, two trucks in the cab length over these two trucks which would mean the transformer, switching apparatus, blowers, and so forth, for these two units would be a package. This would mean building the locomotive in multiples of two trucks and the 2,813 horsepower locomotive shown in Table I in the paper would be dropped out, but if steam and electric locomotive builders are going to get into a competitive position with Diesels they will be wise to take a page from the Diesel book and stop making custom-built engines. Everyone knows the resistance of the Diesel builders to changes but it is obvious that locomotives simply cannot be built cheaply in any other way. It is a well-known fact that steam locomotive costs have suffered because one railroad will usually not accept a locomotive designed for another road; in fact, repeat orders on the same railroad call for engineering changes that affect costs.

There is an advantage in manufacturing the cab apparatus in package units aside from lower production costs. I refer to the ability to bring the train in with fractional power in case of a failure. For example, if a traction motor is lost on the 7,500 horsepower unit shown, it is presumed that that truck would be cut out and the train brought in with 7/8 of the engine. If a transformer is lost, assuming the cab apparatus is made up of package groups connected in parallel, each group serving two trucks, the locomotive could still haul its train in with three-fourths of its rated power. Everyone knows that this flexibility has been a great thing for the Diesels; many a train has been brought in on time with a Diesel engine inoperative and the Diesel gets credited with a perfect performance, which, of course, is right and proper from an operating standpoint.

We would anticipate some engineering difficulties in building the 2,813 and 3,750 horsepower locomotives in the table in which no equalization is indicated between trucks. The authors state that by using soft springs the weight transfer can be held to less than 5 per cent. The writer has had some experience with weight transfer on steam locomotives; there are intangibles in this problem that, to say the least, are difficult to resolve. Even the designs with equalizers would have to be carefully studied as there is not much background for this

construction and heavy equalizers appear to have a time lag in their response and this may be enough to start a slip. Soft springs undoubtedly are a "must" but there are worn wheels and new wheels to consider and it seems to the writer that the best protection is to undermotor the axles to, say, a maximum of 400 horsepower per axle rather than the 469 horsepower suggested.

H. F. Brown (New York, New Haven and Hartford Railroad, New Haven, Conn.): Brecht and Kerr have made a real contribution to the technical bibliography of electric locomotive design in their paper.

It is of especial interest to learn that the a-c motor design has progressed to the extent that it can be applied to a standard truck design almost interchangeably with the d-c motor design, as to weight, dimensions, and performance. The relative costs given of the various types of locomotives (steam, Diesel-electric, and electric) are of exceptional interest, and worthy of a little discussion to see where each fits into the modern transportation picture.

Applying these cost ratios to new locomotives, all having approximately the same rail horsepower, a set of comparative costs per locomotive per year can be set up as in the Table I of this discussion. It will be assumed that power costs for all three types are about equal. This is always debatable, but if there are known differentials favoring one type of motive power over another, these can be applied later. It will also be assumed that engine-house expenses are about the same for Diesel-electric and straight electric, and that these are lower than for steam for the same service. It will also be assumed that operating labor expenses are the same for all three types. On the basis of these assumptions, Table I of this discussion indicates that it may be cheaper to own and maintain a steam locomotive if mileages of more than 100,000 per year per locomotive cannot be attained. If greater annual mileage and more intensive use can be made with certain locomotives, they can be Diesel-electric, with marked economy over steam; but since certain locomotive services have low annual mileage, it is not always economic to completely Dieselize unless it can be shown that the average mileage of all locomotives is 100,000 per year or more, or unless there are other compelling economic factors.

Turning now to the comparison between Diesel-electric and straight electric, it will be seen that even with very low annual mileages per locomotive, it is always cheaper to use the electric locomotive where electrification of the line already exists. In other words, Diesels cannot be operated economically into electrified territory. Similarly, an existing electrification might be profitably extended, if more annual miles per electric locomotive could be realized by so doing, as compared with Diesel locomotives unable to make a much larger annual mileage per unit, in the same service.

Where a new electrification project is being considered, the number of locomotives required, multiplied by the annual saving per locomotive of electric over Diesel as given in the following tabulation, for the expected locomotive miles per year, is a fair index of the amount of capital which could be invested in electrification over and above the locomotive costs.

Table I

	Diesel	Electric	Steam
Cost Ratio	100	68	50
Rail Horsepower	3,750	3,750	3,750
Cost per hp	$110	75	55
Cost per Locomotive	$413,000	$281,000	$206,000
Fixed Charges at 10% per year	41,300	28,100	20,600
Assumed Average Maintenance cost per locomotive mile over life	30¢	20¢	50¢
Fixed Charges per locomotive mile			
@ 50,000 mi./yr.	82.6¢	56.2¢	41.2¢
@ 100,000 " "	41.3¢	28.1¢	20.6¢
@ 150,000 " "	27.5¢	18.7¢	13.7¢
@ 200,000 " "	20.7¢	14.1¢	10.3¢
@ 250,000 " "	16.5¢	11.3¢	8.3¢
@ 300,000 " "	13.8¢	9.4¢	6.9¢
Cost of owning and maintaining each locomotive per year			
Fixed Charges plus (maintenance per mile × mile per year)			
@ 50,000 mi./yr.	$56,200	$38,000	$45,600
@ 100,000 " "	71,300	48,100	70,600
@ 150,000 " "	86,500	58,100	95,500
@ 200,000 " "	101,400	68,200	120,600
@ 250,000 " "	116,000	79,000	145,500
@ 300,000 " "	130,000	81,000	165,600

Miles Per Locomotive Per Year	Annual Saving in Fixed Charges and Maintenance Per Electric Locomotive Over Diesel
50,000	$18,200
100,000	23,200
150,000	28,200
200,000	33,200
250,000	38,200
300,000	43,200

For example, if a certain railroad division required 50 locomotives for its operation, and they could average 100,000 miles per locomotive per year, the saving in electric operation over Diesel would be 50×$23,200 or $1,160,000. If the line could be electrified for, say, ten times this amount, or for $11,600,000, it might be a very good investment. This points to a much shorter line, with much heavier traffic, or with a cheaper electrification, than the one discussed by Kimball and Holm.[1]

Dense traffic always favors electrification; light or medium traffic is harder to prove in, except in the case of an extension to an existing electrification, if and when more locomotive miles per unit can be realized. In such a case the savings, capitalized, of electric locomotives over Diesels, as previously outlined added to the capital costs of Diesel locomotives required, would justify even greater capital expenditures for such an electrification.

One of the principal reasons why Diesels are being purchased in preference to electrifying the railroad is that they can always be bought a few at a time with equipment trust certificates, and these first few units can be worked very intensively; whereas electrification must be done over a definite section of the line all at once, which requires a considerable amount of new capital, in addition to that required for the electric locomotives, all of which must also be purchased at the same time.

Brecht and Kerr have stimulated a new interest in electric motive power by their paper. If the maintenance costs mentioned of 1/3 to 1/2 that of maintaining Diesels or reciprocating steam engines of the same horsepower can be realized, the savings mentioned in this discussion can be much larger, as the maintenance costs mentioned herein for electric are assumed to be 2/3 that for Diesels.

W. A. Brecht and Charles Kerr, Jr.: C. K. Steins, in his discussion of this paper, points out the necessity for standardization to reduce first cost, and also, the desirability for building electric locomotives so that a road failure of any particular part will not prohibit the locomotive completing its assigned run. The authors thoroughly agree with Steins' remarks.

In connection with Steins' comments about weight transfer, the control on these locomotives has been designed to minimize wheel slippage and the authors believe that this factor can be minimized to satisfactory limits.

H. F. Brown has submitted some very pertinent comments regarding the economies of electric operation. A discussion of over-all economies was beyond the scope of our paper and we feel that he has made a very excellent contribution by including such discussion.

D. R. MacLeod has raised several objections to the authors' proposed standardized design of electric locomotives. Many of his objections we do not agree with, but it must be realized that any standardization program always requires certain sacrifices in design. It is our contention that electrification needs, above all else, a reduced first cost, and this can only be achieved by standardizing its component parts. The advantages of so doing greatly outweigh the disadvantages.

Reference

1. Railroad Electrification Energy Conversion and Transmission Costs, R. L. Kimball, J. G. Holm, AIEE Transactions, volume 67, 1948, pages 219-30.

A 10,000-Kva Series Capacitor Improves Voltage on 66-Kv Line Supplying Large Electric Furnace Load

B. M. JONES
FELLOW AIEE

J. M. ARTHUR
ASSOCIATE AIEE

C. M. STEARNS
NONMEMBER AIEE

A. A. JOHNSON
MEMBER AIEE

Synopsis: Duquesne Light Company placed in service on October 26, 1947, the largest kilovolt-ampere and highest voltage-series capacitor in the world, to improve voltage regulation and alleviate voltage flicker at a large steel mill and on the line serving it. The capacitor has a nominal rating of 10,000-kva and was installed on a 66-kv line. It was built by the Westinghouse Electric Corporation. This paper presents the calculated results of system studies made prior to the authorization of the series capacitor and the reasons for selecting the series capacitor. In addition, the results of staged tests showing the starting of a large induction motor and the energizing of the power transformers at the load with the capacitor in service are included. The improvement in utilization voltage is also shown.

THE 66-kv 14-mile 4/0 line, to which the capacitor is connected, is a direct open-wire circuit from the Phillips power plant to the 66-kv bus of the Crucible Steel Company. The steel company receives its power from the line through four step-down transformer banks operating in parallel. The banks have a total name-plate rating of 44,200 kva and are connected 66,000/12,000 volts; the windings are connected delta on the high side and wye on the low side, with the low side neutral solidly grounded.

Figure 1 shows the 66-kv transmission lines, substations and generating stations of the Duquesne Light System, with the Phillips–Crucible line and the series capacitor substation (Kennedy Substation) at the left center.

The Phillips–Crucible line was recently tapped at a point near Crucible to provide standby service for six 22-kv customers and a 4-kv substation in the Midland district through a 66/22 kv bank shown on Figure 1 at the left center. The Midland district load was served normally by one long 22-kv line with severe exposure. This line is not shown on Figure 1. With the series capacitor in the Phillips–Crucible line, it is possible to supply the Midland district normally from the 66-kv line, which was not possible because of wide voltage regulation and severe voltage flicker prior to the installation of the series capacitor.

Customer's Load

The present load at Crucible consists mainly of four 10,000-kva electric arc furnaces, one 4,000-horsepower induction motor (motor-generator set), one 800-horsepower synchronous motor, two 3,000-horsepower synchronous motors, and two 1,000-kva synchronous motor-generator sets. Additional load is expected in the future.

The electric arc furnaces cause rapidly fluctuating loads to be drawn from the Duquesne Light System. When the furnaces melt down, the load frequently exceeds the normal 15-minute billing load and at a lower power factor. The 15-minute billing load in general determines the effective load on the supply line and transformers. The instantaneous load, which may last only a few minutes, determines the voltage regulation at the customer's 11-kv bus and at other points along the supply line.

The Crucible 15-minute billing load is 28,000 kw at 87.5 per cent power factor or 32,000 kva. The instantaneous load is 28,600 kw at 78.3 per cent power factor or 36,500 kva.

Voltage and Flicker Conditions at Crucible Prior to Series Capacitor Installation

The Crucible instantaneous peak load of 28,600 kw at 78.3 per cent power factor caused Crucible voltage to drop from the no load value of 11,900-volts to 9,800-volts, or 119-to-98-volts in low voltage or 110-volt terms. Hereafter, voltages will be expressed in 110-volt terms for convenience, and are equivalent to 11,000 volts divided by 100 or 66,000-volts divided by 600. Occasionally, the Crucible voltage ranged from 121 volts at no load to 98 volts at full load. Figure 2 (curve *A*) shows 11-kv voltage at Crucible before the series capacitor was installed.

In addition to the excessive regulation, the furnace loads fluctuate so rapidly that they cause severe instantaneous voltage drops or voltage fluctuations on the 11-kv bus and 66-kv bus at Crucible, and poor voltage regulation and less severe voltage fluctuations at other points along the 66-kv line. These voltage fluctuations are generally termed light flicker and were calculated to be nine volts on the customer's 11-kv bus in 110-volt terms. These fluctuations are of varying frequency and may reach a rate of one per second.

The wide regulation range is not too serious as far as furnace operation is concerned, but it has caused some trouble in motor operation. The condition has existed for some time, but it was not possible to remedy it during the war. The

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B. M. Jones is planning engineer, J. M. Arthur is development engineer, and C. M. Stearns is senior development engineer with the Duquesne Light Company, Pittsburgh, Pa.; A. A. Johnson is central station engineer, with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

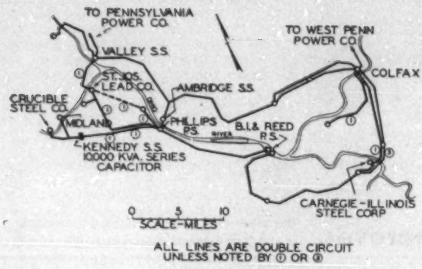


Figure 1. Power system diagram showing 66-kv lines and power stations of Duquesne Light Company

Kennedy substation and the 66-kv line between Phillips Power Station and Crucible are shown at left center

initial requirements for the series capacitor were established on December 14, 1945, and actual authorization by letter was given on January 4, 1946.

Voltage and Flicker Conditions at Crucible With the Series Capacitor In Service

After studying several types of corrective equipment, the 10,000-kva series capacitor was selected as the most economical and satisfactory device for improving voltage and reducing lamp flicker. A more detailed discussion on the types of equipment considered will be reviewed later in this paper.

With the series capacitor in service, it has been calculated that the 11-kv bus voltage at Crucible will range (total) between 121 volts and 110.5 volts, and the lamp flicker voltage will be 4.5 volts. Figure 2 (Curve *B*) shows the 11-kv voltage at Crucible after the capacitor was installed. Figure 3 shows a graph of the calculated voltage at various points on the line between Phillips Power Station and the 11-kv Crucible bus before and after the series capacitor was placed in service.

During the past few years, Crucible has experienced some unsatisfactory motor operation (running and starting) during heavy furnace load periods, because of low voltages and voltage unbalance caused by furnace operation. With the series capacitor installation, these troubles should be entirely eliminated.

Voltage at Midland

Reduced voltage regulation and reduced lamp flicker along the Phillips–Crucible line on the load side of the capacitor enables the Duquesne Light Company to use the tap from the Phillips–Crucible line through a step-down bank for normal service to the Midland district. The 10,000-kva step-down transformer bank is connected 69,300/24,250. The present single long 22-kv line can now be retained as a standby.

Figures 4*A* and 4*B* show a chart of the voltage on the 22-kv bus at Midland substation when supplied from the 66-kv line before and after the capacitor was installed, respectively.

Methods Considered For Correcting Voltage

For improving the voltage regulation and flicker conditions at Crucible, the following types of corrective equipment were considered.

1. Voltage regulator (step-type) for motor load alone.
2. Synchronous condenser.
3. Switched shunt capacitors.
4. Second 66-kv line.
5. Series capacitor.

A brief discussion of the types of corrective equipment considered follows.

Voltage Regulator

Voltage regulators require a change of voltage to actuate them, and then frequently by the time voltage is corrected the excess current demand has ceased and over-compensation in voltage results. In addition, a time delay device must be used to reduce the number of operations. Therefore, a voltage regulator is not suited for regulating rapidly changing voltage or correcting flicker.

A small laboratory regulator was designed and constructed by the light company to indicate the number of times it would actually need to operate with certain time delay and with various amounts of voltage correction. Operation of this regulator indicated that, for instance, with 10 seconds time interval between tap changes, the tap changes per year would vary between 100,000 and 400,000 for ±4 volts and ±2 volts, respectively. It was believed that operations of this magnitude would cause excessive maintenance.

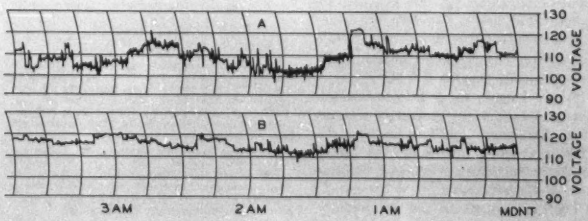
The cost of reconnecting the three circuits supplying the motor load to a regulator would be expensive. It was also agreed that all load, including furnace load, should be regulated and for the reasons cited, the regulator idea was dropped.

Synchronous Condensers

Synchronous condensers, connected at the load, unlike voltage regulators, would be effective in reducing lamp flicker as well as voltage regulation. With instantaneous changes in load, the inherent characteristics of synchronous condensers help to limit voltage change by supplying part of the reactive kilovolt-amperes required, thereby reducing light flicker. This is accomplished because of the instantaneous change in output of a condenser which takes place with sudden changes in terminal voltage. A synchronous condenser to provide the same improvement in voltage regulation as the series capacitor would need to be approximately 50 per cent greater in kilovolt-ampere capacity. The improvement in light flicker with this size condenser is not as great as that with the series capacitor. The installed cost, losses, and operating and maintenance costs for the synchronous condenser would be considerably higher

Figure 2. Crucible 11-kv voltage charts with heavy furnace load

(A) Before series capacitor installation
(B) After series capacitor installation



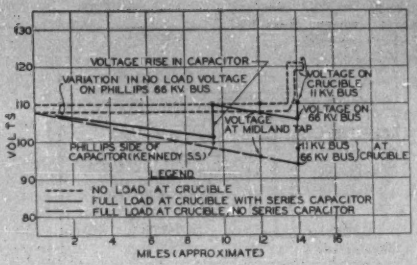


Figure 3. Voltage diagram showing regulation on Phillips–Crucible 66-kv line and on Crucible 11-kv bus before and after series capacitor installation

than for the series capacitor. Unless two condensers were installed, the problem of breakdown would be present. A buffer reactor could not be used with the condenser, since the problem was to improve voltage on the customer's bus as well as on the supply line.

Switched Shunt Capacitors

Consideration was given to connecting a group of shunt capacitors to each of the four furnace circuits, to be switched with the furnace, and to connecting another unswitched group to the 11-kv bus. Such an arrangement did not prove feasible, since furnace refining loads are low and at a good power factor and capacitors would be in service when not needed, thus possibly increasing the voltage regulation band. Furthermore, shunt capacitors would aggravate light flicker since the ampere load taken by the capacitors varies directly with the voltage. Switching of shunt capacitors to follow rapidly changing loads in any manner other than described before would be impractical.

Second 66-kv Line

A second 66-kv line from Phillips to Crucible was also considered as a method of alleviating the voltage and flicker problem. With two lines to Crucible, the minimum voltage on the 11-kv mill bus would be 104 volts compared with 110 volts delivered by the series capacitor. The second line would be expensive.

Series Capacitor

A series capacitor was selected since it is fundamentally suited to reducing flicker and improving voltage, doing both efficiently and at relatively low cost. It is inherently automatic in operation, since it introduces a voltage which leads the current 90 degrees and is directly proportional to the magnitude of current. The capacitive reactance voltage instantaneously compensates for a like amount of line inductive reactance voltage for all values of the current, alleviating rapid voltage fluctuations and reducing voltage drop.

Since a series capacitor reduces the impedance of a line, it increases, in effect, the capacity of the line because of increased utilization voltage. Stated another way, increased kilovolt-ampere loads can be carried with the same voltage drop, or the same kilovolt-amperes can be carried with less current. With the 10,000-kva series capacitor in the Phillips–Crucible line, current values are reduced 10 per cent for the same delivered kilovolt-ampere load.

If the Crucible load increases, and a second line from Phillips to Crucible is required to carry the line amperes, the series capacitor will also be needed and can be used advantageously. This can be done by carrying the second line through the capacitor and increasing the thermal rating and reducing the ohmic value of the capacitor.

Size and Location of Series Capacitor

Size of Capacitor

The proper size of a series capacitor depends in general on:

1. The maximum ampere load of the circuit, giving consideration to possible growth.
2. Amount of voltage improvement desired.
3. The ohms reactance of the line and transformer banks involved.

Although the present ampere load at Crucible is less than the 500-ampere rating of the 66-kv line, it is expected that the load will grow. Therefore, the rating of the capacitor was selected as 500 amperes.

Calculations indicated that the inductive reactance from Phillips Power Station to the Crucible 11-kv bus must be reduced approximately 14 ohms in order to raise the minimum voltage to 110 volts with loads of about 30,000 kw at around 80 per cent power factor. A capacitive reactance of 14 ohms would compensate for the reactance of the Phillips–Crucible line of 11.49 ohms plus most of the system reactance at the Phillips bus of 3.59 ohms. A 14-ohm series capacitor on a circuit carrying 500 amperes has a voltage drop across the capacitor of 7,000 volts per line. Since 7,000-volt capacitors do not exist, or at least are not standard capacitors, 3 2,400-volt 15-kva capacitors in series were selected.

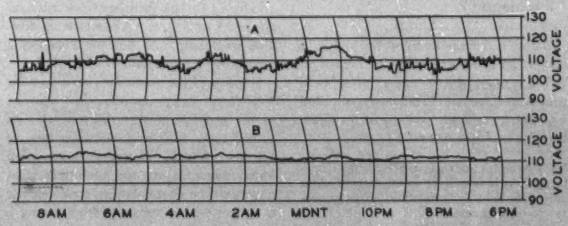
Capacitors of 2,400 volts allow the use of standard protective devices, such as protective gaps and by-pass switches. The 2,400-volt capacitors will also be useful elsewhere on the Duquesne Light System should the capacitor installation ever be abandoned.

After the 2,400-volt 15-kva unit capacitor was decided upon, the total number of units was determined by using the rated ampere values of each unit, namely 6.25 amperes. Since the series capacitor must have a 500-ampere rating, 80 units were provided in parallel. The nominal impedance of each unit is 384 ohms, and the total impedance of 80 such units in parallel would be 4.8 ohms or 14.4 ohms for three groups in series.

Standard 15-kva capacitor units have a guaranteed tolerance in rating of minus 0 per cent and up to plus 13 per cent. This means that the impedance of each

Figure 4. Midland 22-kv voltage charts with heavy furnace load at Crucible

(A) Before series capacitor installation
(B) After series capacitor installation



15-kva unit may vary between 384 ohms and 340 ohms. Assuming an average distribution of 362 ohms, the total capacitor impedance is approximately 13.5 ohms.

Calculations indicate that with a 500-ampere 13.5-ohm series capacitor, the present instantaneous load of 28,600 kw at a power factor of 78.3 per cent would result in a maximum regulation range of 121 to 110.5 volts. This was considered satisfactory.

Location of Capacitor

Where load is not distributed along the line, a series capacitor can be placed in general at any location on the supply side of the load. The capacitor should be located so that the maximum fault current on the load side of the capacitor does not exceed the momentary rating of the protective devices. Furthermore, it should be so located that the terminal voltage on the load side does not exceed the voltage of lightning arresters normally used by the power company. Accessibility is an important point in locating the capacitor.

The following table shows the impedances of the Phillips–Crucible line ahead of the capacitor and from the capacitor to the Crucible 11-kv bus.

0+j 3.59	Phillips bus
2.59+j 7.67	Phillips to Kennedy
0.73+j 2.05	Kennedy to Midland tap
0.62+j 1.77	Midland tap to Crucible bank
0.69+j 8.60	Crucible bank
4.63+j 23.68	Total
0−j 13.50	Kennedy Substation (Series Capacitor)
4.63+j10.18	Net impedance

Diagram of Series Capacitor

Figure 5 shows the simplified connections of the series capacitor shunted with a resistor. The resistor is necessary to solve the series capacitor application problems. A detailed description of the capacitor and its protective equipment is given in companion papers.[1,2]

Series Capacitor Application Problems

Before a definite decision was made to install the series capacitor, an investigation was made of series capacitor application problems and troubles experienced by other power companies. These troubles were found to be:

1. Distorted and excessively large transformer exciting current upon energizing an unloaded bank. This condition is known as "ferroresonance."

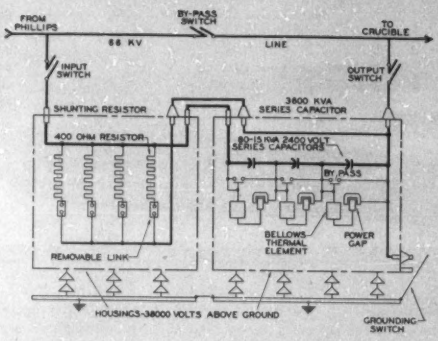


Figure 5. Series capacitor diagram showing three groups of capacitors with their protective devices, four 400-ohm shunting resistors, 66-kv line airbreak switches, and grounding switch

2. Hunting of lightly loaded synchronous motors.

3. Self-excitation of induction motors and synchronous motors which start as induction motors.

Ferroresonance

When voltage is first applied to an unloaded transformer, a high inrush of current may result if this voltage is applied near the zero point of the voltage wave. This is because flux in the transformer tends to double in value during the first half cycle, and with high values of flux the effective inductance of a transformer becomes very low. Because of low inductance, excessive currents (magnetizing currents) may flow for several cycles after which the transformer draws its usual low value of exciting current. However, it has been found that occasionally when a transformer is supplied through a series capacitor without a shunting resistor, the initial excessive currents which may normally prevail for a few cycles may continue to persist in the steady state. This current may be greater than rated current of the bank, and is apparently of a low frequency, resulting in badly distorted current and voltage waves. These abnormal steady-state currents flow when the voltage is applied near the zero point of the wave just as the high transient occurs for this condition in the ordinary case without the series capacitor. A complete qualitative explanation of this phenomenon is covered in a paper published in 1937.[3]

Because of the nonlinear characteristics of a transformer, mathematical analysis of the problem is very difficult, and no satisfactory analytical solution has as yet been found. However, it has been found from experience that ferroresonance can be eliminated by shunting the capacitor with a resistor or by having a certain amount of load on the load side of the series capacitor.

Should ferroresonance occur on the Phillips–Crucible line with the series capacitor, the resistor shunting the capacitor to prevent self-excitation of motors, covered later in this paper, will also prevent ferroresonance.

Hunting of Synchronous Motors

A synchronous motor when lightly loaded is sensitive to system disturbances, such as switching, changes in load, pulsating load, and changes in excitation. The main factor in determining whether a synchronous motor will hunt with the installation of a series capacitor, is whether the ratio of system resistance to system reactance up to the motor's terminals is large or small. After installing a capacitor, if the ratio of line resistance to equivalent reactance is considerably higher than unity, then there is a good possibility of synchronous motors hunting. On the other hand, if the ratio is low, less than unity, then there is little possibility of such a phenomenon occurring.

Several years ago, Duquesne Light Company installed a 130-kva series capacitor on a 4-kv distribution circuit and found that the capacitor caused a 225-horsepower 1,200-rpm 2,300-volt synchronous motor to hunt violently at no load. In this particular case, the most economical solution was to connect the motor to a 22-kv circuit through a step-down bank.

In the distribution circuit mentioned previously, the ratio of resistance to reactance was found to be about 4.0. However, in the Phillips–Crucible circuit and system ahead of it, the ratio of resistance (7.5 ohms) to equivalent reactance (12.0 ohms) up to the motor terminals with capacitor in service was

found to be the low ratio of approximately 0.6. No hunting is therefore expected.

Self-Excitation of Motors

Induction motors and synchronous motors which start as induction motors may fail to pull up to normal operating speed on starting after a series capacitor is installed on the circuit. This particular phenomenon is called "self-excitation." A motor which self-excites comes up to only partial speed on starting, and locks in at that reduced speed. At this reduced speed, the motor is unstable and large resonant currents flow.

Calculations and studies indicated that from a theoretical standpoint a minimum of 100 ohms in shunt with the series capacitor would be needed to prevent self-excitation when the 4,000-horsepower induction 514-rpm motor was started. These calculations are described in a companion paper.[1]

Resistor, Circuit, and Capacitor Losses

Load Division Through Resistor and Capacitor

If the 10,000-kva series capacitor is shunted with a resistor, a certain amount of load current will by-pass the capacitor and flow through the resistor, causing I^2R heat loss. Table I shows the calculated line amperes and the amperes calculated to flow through a 100, 200, 400 or 800 ohm resistor and the capacitor with a load of 28,000 kw at 87.5 per cent power factor.

Losses

In determining the annual kilowatt-hour loss of the line, capacitor and resistor, the customer's annual load factor was assumed at 40 per cent, which is the approximate average of the annual load factors at Crucible for the past seven years. Because of the squared function, the load factor of the losses is less than the load factor of the load. The approximate relation is that for a load factor of 40 per cent, the copper loss load factor is approximately 24 per cent.

Table II shows the I^2R line loss (both the maximum one-hour loss in kilowatts and the annual loss in kilowatt-hours) before the capacitor was installed, and after the capacitor was installed and shunted with a 100, 200, 400, 800 or an infinite ohm resistor.

Table II also shows similar resistor losses for each size of shunting resistor.

The capacitor has a loss of 1/3 of 1 per cent at rated load of 500 amperes. This loss is converted to an equivalent resistance at full load. The losses are therefore proportional to the square of the current flowing through the capacitor. The capacitor losses are shown on Table II.

Switching Equipment for Shunt Resistor

Table II shows that the resistor losses are quite substantial. These losses could be saved by switching the resistor into service only when the large 4,000-horsepower induction motor is started and when the line is energized.

Switching equipment was not provided because it was believed that the need for the correct resistor size should first be determined by tests. However, a preliminary scheme for switching consists of one 3-pole gang-operated 69-kv 600-ampere disconnecting switch with motor-operated mechanism. This switch could be operated by "carrier current" remote control equipment from the customer's motor room and Phillips Power Station.

Summary

The line current is reduced from 338 amperes to 308 amperes with the series capacitor in service but not shunted with a resistor, when customer's load is 28,000 kw at 87.5 per cent power factor. Table II points out that with the installation of the series capacitor (not shunted with a resistor or with the shunting resistor switched), Duquesne Light will save approximately 250 kw in maximum one-hour loss and 500,000 kwhr in annual losses, as compared with conditions with no capacitor. Table II shows that the total losses with the capacitor, with a 400-ohm shunting resistor or larger in ohmic value, is less than the line loss with no capacitor. Thus, voltage corrective equipment with the 400-ohm shunting resistor or larger is installed without increasing system losses.

On the basis of average system generating costs per kilowatt-hour, system diversity, and capital costs per kilowatt of generating capacity, the losses of 100- and 200-ohm resistors are such that it would be economical to install switching equipment in the resistor circuit to save the losses. For a resistor of 400 ohms and greater, the switching equipment cannot be economically justified.

Series Capacitor Test Program

While calculations indicate that subharmonic frequencies would be present in starting the 4,000-horsepower induction motor with the capacitor in service, there are certain factors that cannot readily be taken into account in calculations. Calculations usually indicate that a resistor of much lower ohms shunting the capacitor is needed than is actually required. Thus, it was expected that a resistor of a higher ohmic value than the calculated figure of about 100 ohms would be satisfactory. Because of this, 4 400-ohm 7,200-volt resistors were provided, as described in a companion paper,[1] with connections to give an immediate range of 100 to 400 ohms. A range of 400 to 1,600 ohms can also be obtained.

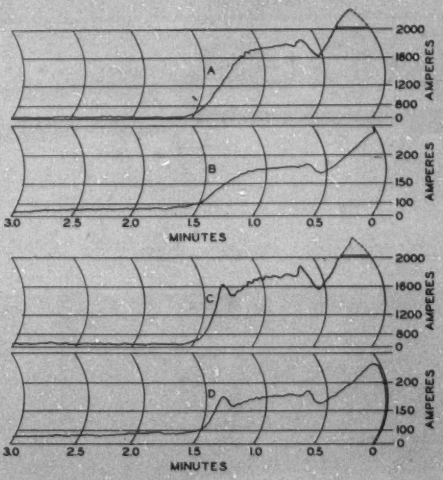


Figure 6. Motor starting charts of 4,000 - horsepower induction motor with no furnace load and 1,700 kva of other motor load

(A) Rotor current before capacitor installation
(B) Stator current before capacitor installation
(C) Rotor current with capacitor and 400-ohm resistor
(D) Stator current with capacitor and 400-ohm resistor

Motor Starting Tests

A test program was prepared which covered starting the motor with no capacitor and starting the motor with 100 ohms, 400 ohms, and infinite ohms shunting the capacitor, with a customer station load of 15,000 kva or more, and with no station load for each of the conditions.

Line Energizing Tests

The program also included the energizing of the 66-kv line from Phillips Power Station with bank magnetizing current only with no capacitor, with the capacitor in service shunted with a 100-ohm resistor, and without a resistor.

Meters for Test Program

An Esterline ammeter and voltmeter, and an oscillograph were installed at Phillips Power Station. Seven Esterline meters were installed at the customer's substation to record station load and 11-kv and 66-kv voltages. Six Esterline meters and an oscillograph were installed in the stator and rotor leads of the 4,000-horsepower induction motor and in the leads to an auxiliary power bank supplying a 1,000-horsepower synchronous motor-generator set.

Electrical quantities at Kennedy substation were not measured because of the difficulty of connecting instrument transformers. However, Veeder counters are permanent equipment on the gap by-pass switches to record switch operations.

Table I. Line, Resistor, and Capacitor Amperes With Various Shunting Resistors

Resistor Sizes	Crucible Load	Power Factor	Line Amperes	*Capacitor Amperes	*Resistor Amperes
Ohms	Kw	Per Cent			I
0	28,000	87½	338	0†	0
100	28,000	87½	313	310	42
200	28,000	87½	311	311	21
400	28,000	87½	309	308	10½
800	28,000	87½	308	308	5¼
None	28,000	87½	308	308	0

* Capacitor + Resistor Amperes Must Be Added Vectorially To Equal Line Amperes.

† Capacitor short-circuited.

Results of Series Capacitor Tests

Motor Starting Tests

Motor starting tests were conducted as outlined, and the 4,000-horsepower motor came up to normal operating speed in all cases. Only the more important records are reproduced in the figures which follow.

Figure 6, curves *A* and *B* show the rotor and stator currents for normal motor starting without the series capacitor. Curves *B* and *C* show the rotor and stator currents for starting the motor with the capacitor in service, shunted with a 400-ohm resistor and with practically no plant load.

For the motor starting test without a resistor around the capacitor and with substantial plant load, the rotor and stator currents were normal and without evidence of other frequencies. However, when most of the plant load was dropped and the motor restarted from rest, subsynchronous frequencies were present. The subnormal frequencies caused two of the by-pass gaps to flash over on each of two phases of the series capacitor. When the gaps operated, the by-pass switches closed.

The gaps flashed over because at the subsynchronous frequency of about 5 cycles per second the ohms in the series capacitor are 12 times their 60-cycle value. Thus, since the gaps were set to flash over at a voltage corresponding to 1,000 amperes (60 cycles), only about 85 amperes of 5 cycle current were required to break down a gap.

Table II. Line, Resistor and Capacitor Losses

	Maximum Hour Kilowatt Loss	Annual Kilowatt-Hour Loss
Line Loss—No Capacitor	1,585	3,330,000
Line Loss—With Capacitor	1,360	2,860,000
100 Ohm Shunting Resistor	528	1,113,000
Capacitor Loss	13	27,000
Total	1,901	4,000,000
Line Loss—With Capacitor	1,345	2,830,000
200 Ohm Shunting Resistor	264	555,000
Capacitor Loss	13	27,000
Total	1,622	3,412,000
Line Loss—With Capacitor	1,325	2,781,000
400 Ohm Shunting Resistor	132	278,000
Capacitor Loss	13	27,000
Total	1,470	3,086,000
Line Loss—With Capacitor	1,320	2,780,000
800 Ohm Shunting Resistor	66	139,000
Capacitor Loss	13	27,000
Total	1,399	2,946,000
Line Loss—With Capacitor	1,320	2,780,000
No Shunting Resistor	0	0
Capacitor Loss	13	27,000
Total	1,333	2,807,000

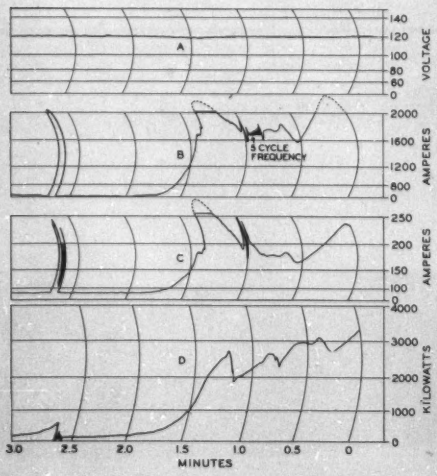


Figure 7. Motor starting charts of 4,000 horsepower induction motor with no furnace load and 2,500 kva of other motor load, the capacitor in service and no shunting resistor

(A) Stator line to line voltage
(B) Rotor current
(C) Stator current
(D) Stator 3-phase kilowatt input

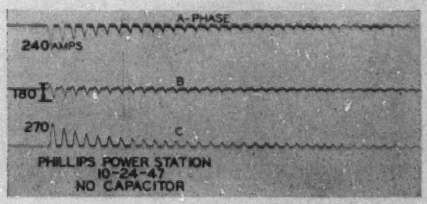


Figure 8. Energizing Crucible and Midland unloaded banks (54,000 kva) with no capacitor, showing amperes per phase at 66 kv

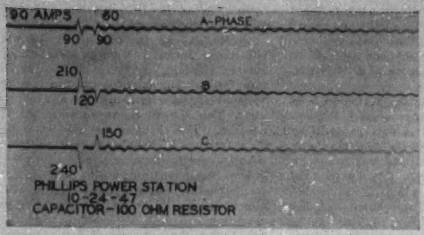


Figure 9. Energizing Crucible and Midland unloaded banks (54,000 kva) with capacitor, and 100-ohm shunting resistor, showing amperes per phase at 66 kv

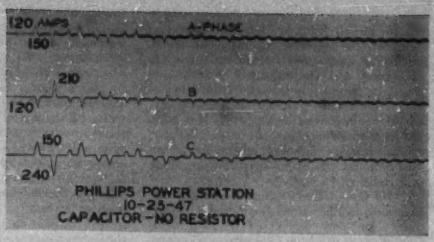


Figure 9. Energizing Crucible and Midland unloaded banks (54,000 kva) with capacitor and no shunting resistor, showing amperes per phase at 66 kv

Figure 7 is the record of quantities which were measured during this test. The rotor current record, even though it is from a high speed recording chart, gives evidence of some transient condition. The current records also give some indication when the gaps in the capacitor broke down closing the by-pass switches and when the by-pass switches reopened. The breakdown of the gaps possibly accounted for the motor coming up to speed.

Calculations indicated that the subharmonic frequencies would occur during the first 30 seconds. The motor takes 105 seconds to reach normal speed. Actual tests indicated that the condition did not take place until after the first 30 seconds. Oscillograph films used to record the tests were only eight feet long, and 30 seconds of record was the best that could be obtained; therefore, the subnormal frequencies were not recorded on the films.

Energizing Tests

Figures 8, 9 and 10 show an oscillograph record of the currents in the three phases of the Phillips–Crucible line as recorded at Phillips during the three energizing tests. It will be noticed that with a 100-ohm resistor shunting the capacitor, the initial inrush current seems to be damped out, as compared with the inrush current with no capacitor. With the capacitor in service and no shunting resistor, the energizing current is very erratic, and several of the gaps at the series capacitor flashed over. However, the steady-state current magnitude was of insufficient value to maintain the arc, and the gaps went out on their own account without closing the by-pass switches.

Summary

Motor starting tests with the capacitor in service and no shunting resistor and no other station load have indicated that a resonant condition exists. The capacitor, therefore, was shunted with a 400-ohm resistor at the end of the test and left in service. This 400-ohm resistor should also keep the gaps from flashing over in case of picking up the unloaded bank.

Future tests may be conducted to determine whether or not a 600- or 800-ohm resistor will be satisfactory for both motor starting and line energizing, since a higher value of resistor will reduce losses.

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Discussion

E. C. Starr (Oregon State College, Corvallis, Oreg.): Many transmission engineers have followed the installation and testing of this series capacitor with keen interest. It was recognized that the large motor load involved presented special problems and we were anxious to learn the actual degree of resistance shunting required to produce successful operation under adverse motor-starting conditions. It now has been shown experimentally that this shunting resistor can be large enough to result in total circuit losses that are less than those existing with no capacitor and that the load voltage level and regulation have been improved greatly.

The conservatism of the engineers on this project in providing adequate shunting conductance to insure successful operation is appreciated by those of us who are thinking in terms of much larger series capacitors at higher transmission voltages. A questionable degree of success in this instance would have impeded progress in the development and acceptance of the larger units. The present highly successful application will facilitate the next step.

We are thinking not only of series capacitors for voltage improvement on short lines but particularly for load-limit improvement on long lines. In the present application, quick restoration of the capacitor after the protective device has flashed is not desirable, but in the long-line case, especially when multiple circuits are involved, immediate restoration after fault isolation is critically important. The ideal protective device for this service would be one which incorporates lightning-arrester characteristics. The excess voltage peaks only then would be clipped each half cycle and the capacitor would be restored to service within one-half cycle of the time of fault isolation.

A series damping resistor in the protective circuit designed to limit capacitor voltage to

safe values for maximum fault currents would further improve the performance. Local circuit oscillations during the short-circuiting operations would be damped effectively and the capacitor at no time would be completely removed from the circuit. Transient stability of the system would be improved substantially by both the high speed of capacitor reinsertion and the incomplete removal of the capacitor during faults, most of which are other than of 3-phase character.

In the present and similar cases, it appears that an adequate time delay of reinsertion built into the protective device might eliminate the need for a shunting resistor to control subsynchronous resonance during critical motor starting operations. With this delay, the capacitor would be short-circuited when resonance developed either by gap or by relay action and would not be reinserted until the motor had accelerated beyond the resonant speed.

It is generally understood but again should be emphasized that practical voltage improvement by series capacitors, as in the present case, can be obtained only when the load is definitely of lagging power factor, and when the over-all reactance-to-resistance ratio of the uncompensated circuit is considerably greater than unity.

A. G. Darling (General Electric Company, Schenectady, N. Y.): Since only a few installations of series capacitors have been made with the purpose of improving the voltage regulation caused by arc furnaces, the results of operation of an installation heretofore unreported, may be of interest and use to the industry.

Conditions governing the choice of characteristics of the capacitor will vary with each case. A close analysis of the circuit characteristics and loads as well as the amount of capacitors installed is essential to evaluate the results.

In 1939, some rather complete tests were carefully made on the series capacitor installation of the Braden Copper Company at Rancagua, Chile. The installation and tests were made under the supervision of A. J. Noraeger, chief electrical engineer, by his technical assistant, F. A. Canzi. So far as is known, this was the first attempt to offset rapid load fluctuation of arc furnaces by this method.

Generation of the Braden system in 1939 was in two hydroelectric plants of 47,000 kva total capacity stepped up to a transmission system of 66 kv to serve the general mining area with a 34.5-kv tapped line from two 5,110-kva transformers. The line runs 18 miles to the furnace and plant residential area at Rancagua. The furnace had disturbed the lighting voltages in this area. At the Rancagua Station, one set of main transformers serves the furnace transformers, and another serves the lighting and general shop loads. To correct the regulation of the furnace transformers by 34.5-kv series capacitors would have over-corrected the voltage applied to the lighting and general power transformer primary voltage. Therefore, series capacitors were selected which have capacitive reactance equal to the inductive reactance of the autotransformers supplying the 34.5-kv line and the line itself totaling 20 ohms. The line resistance is 10 ohms. The capacitors selected were standard 2,300-volt 15-kva units, 18 per phase in parallel totalling 780 kva 3-phase 60-cycles. A bypass contactor was relayed to shunt the capacitor at 275 amperes or 5,500 volts across the capacitor and insert the capacitor again when the current falls to 110 amperes.

The load at Rancagua consists of general load whose maximum is 2,000 kw at high power factor, a 1–1$^1/_2$ ton, 800 kva and two 3 ton, 2,000-kva furnace transformers each of which has current limiting reactors giving 35 to 40 per cent reactance on transformer base.

Low voltage shunt capacitors of 1,260 kva and a synchronous condenser of 2,350 kva are connected to the furnace transformer 6.9-kv bus.

The series capacitor is installed near the source end of the 34.5-kva line as better effect was anticipated.

On staged tests wherein the electrodes of the three furnaces were simultaneously shorted, the lighting voltage changed by 39 volts without the series capacitor and by 26 volts when the series capacitor was in service. This showed an improvement of 33 per cent. This simultaneous shorting of the three furnaces is possible but rarely, if ever, occurs under automatic electrode control.

Fast chart, recording meter records, and oscillographic records were taken with alternative operation of the synchronous condenser and the series capacitor when the two 3-ton furnaces were on rough and meltdown loads.

RESULTS OF OPERATION

Equipment in Operation				
Synchronous condenser	off	on	off	on
Series capacitor	off	off	on	on
Shunt capacitor	on	on	on	on
Kilovolts on 6.9-Kv Furnace Supply Bus				
Maximum	7.0	6.7	6.7	6.5
Minimum	6.0	5.8	5.8	6.0
Spread	1.0	0.9	0.9	0.5
Lighting Voltage				
Maximum	116	118	112	115
Minimum	101	106	105	105
Spread	15	12	7	10
Range of rates of change (Volts per second)	16–18	6–16	4–12	5–9
Variation in Illumination by Sight Meter				
Rough melt—foot candles	10–12		5–6	
Meltdown—foot candles	3		1	
Changes per minute	22–29		10–18	

The series capacitor reduced the amplitude of light intensity change to 1/2 and reduced the frequency of objectional changes in intensity to 1/4 or less than it was without the series capacitor.

Visual conditions were improved further

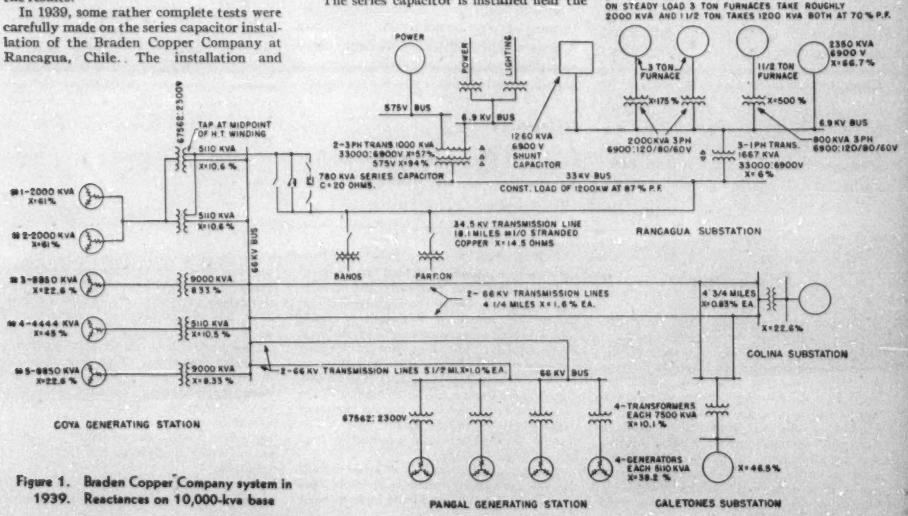

Figure 1. Braden Copper Company system in 1939. Reactances on 10,000-kva base

than the data given indicates as the number of small voltage changes, noticeable as light flicker, were reduced when the series capacitor was in service.

A shunting resistor was not used and no difficulty was encountered in magnetizing transformers or starting of motors or capacitors.

J. W. Butler (General Electric Company, Pittsfield, Mass.): It is gratifying to see the capacitor grow from a piece of laboratory equipment to a major tool at the disposal of system designers and operators in a period of about 20 years. It has been taking on larger and tougher jobs on systems in the last decade to a point where now it can rightfully take its place along with the transformer, circuit breaker, and generator. It is a credit to the application, design, and planning engineers connected with this project to be able to:

1. Sell the idea to their management.
2. Produce and operate successfully the equipment as planned.

I have been associated with capacitors and self-excitation of motors for some time and I can well appreciate the problem confronting the authors of this paper. I have tested in the laboratory and on field installations where these phenomena existed and also have done considerable analytical work on the subject, and I can certainly appreciate the authors' remarks to the effect that there are a lot of things that cannot be taken into account on the analytical work making the accurate prediction of operating results very difficult, if not impossible.

In viewing through an oscilloscope the motor current as the series capacitor ohms are increased distortions appear in the current long before an unstable run-away self-excitation current is obtained. In some set-ups it is only these stable wriggles that appear as the motor comes up to speed, while in other set-ups the circuit becomes unstable which causes the motor to vibrate, become noisy with rotor oscillations and continuous operation of the protective equipment. It is practically impossible to predict the difference between these two cases analytically, so obviously engineers have to play safe in making an application where calculations indicate the possibility of this difficulty.

In the case at hand, it is reported that with no resistor, subsynchronous frequencies existed and the motor experienced difficulty in coming up to speed, but with the 400-ohm resistor it performed satisfactorily, and this value was left in service. I have found that a very high value of resistance is tremendously more effective than infinite resistance, which suggests the possibility of operating this installation at a much higher ohmic value than the 400 arrived at. I understand that 1,600 ohms is obtainable by reconnection. I feel this value has a good chance of working, and if it does work the losses would be reduced to 25 per cent of their present value.

The success of this installation in general should give encouragement to engineers having similar bad voltage conditions. Many of the modern ore reduction processes impose severe requirements of power supplies and this type equipment should prove useful.

Leonard M. Olmsted (Duquesne Light Company, Pittsburgh, Pa.): The series capacitor described in this paper has been in service three months, and it may be interesting to have a preliminary report on its operation. This discussion presents the observations of several operating engineers and the authors of the paper, based upon a series of tests, inspections, and performance of customers' equipment.

The series capacitor has very satisfactorily demonstrated its effectiveness in reducing voltage dips, improving the unbalance in voltage and the general level of voltage. Before the capacitor installation, the large 4,000-horsepower motor tripped occasionally from under-voltage and voltage unbalance conditions caused by electric furnace operation. At times starting had to be delayed until furnace load was reduced.

As a result of the improved voltage, the Crucible Steel Company can take loads for short periods at off peak times considerably in excess of those formerly taken. There was some concern at first because these high loads were in excess of the rating of the line and of the capacitor. Thermal indicating meters installed in the line at its power input end, however, showed that the load lasted only a short time (less than 15 minutes), and occurred only two or three times a week.

Since the capacitor was installed, it has been taken out of service several times for inspection, first at rather short intervals and then at longer intervals. Five weeks have elapsed between the last two inspections. It was believed that inherent troubles in the capacitors themselves would show up rather early. As time goes on, it is hoped to schedule the routine inspections to six months or longer.

There have been minor operating difficulties as can be expected from a new installation. The troubles that have occurred are burned bellows and a few blown fuses on the individual capacitors. The burned bellows have been associated with one or more starts of the 4,000-horsepower motor during a period when the liquid starting rheostat was not allowing proper starting. This permitted a subsynchronous resonant condition to exist which was of sufficient magnitude and duration to damage the bellows operating elements in some of the by-pass switches. At the time this happened, the capacitor was shunted with a 400-ohm resistor which was considered satisfactory for all normal motor starting.

In order to guard against a repetition of damage to the switches in the event of future resonant conditions that may occur, a modification was made in the by-pass switches.

The capacitor fuses operated from overload and not from short circuit, probably during the resonant condition. This could happen because, as the protective gap flashes over, the capacitor discharges like a surge generator through the gap circuit. Several gap discharges could result in sufficient current magnitude to weaken the capacitor fuses.

The troubles that have occurred make it desirable to have some device to indicate whether the by-pass switches have operated and the approximate condition of the individual capacitor fuses without having to take the capacitor out of service. This problem is being studied at the present time and something will be done.

After the by-pass switches were modified, two additional motor starting tests were conducted with a 400-ohm capacitor shunting resistor and no furnace load. In each of these tests, the motor came up to speed in a normal manner.

Particular attention has been given to safeguarding service reliability. This radial line is the only supply to a large steel plant, and was designed with a high impulse level in an effort to provide reliable service. Eight service years without interruption from lightning and with only two cases of minor trouble have demonstrated the soundness of this design. Obviously the capacitor substation could not be permitted to impair this service record; it was designed to maintain the high impulse level and to permit the use of "hot stick" tools for maintenance without requiring a line outage. The selection and arrangement of pole-top switches lightning arresters and connectors reflect these requirements.

B. M. Jones: As referred to in the paper, we explored other methods for correcting this voltage situation, including the installation of synchronous condensers and switched shunt capacitors at the customer's plant, and a second line from our Phillips Power Station to the customer's plant. The selection of the 66-kv series capacitor was made after a thorough analysis of all factors, including installation and operating costs and carrying charges, as well as maintenance difficulties. Interestingly, the synchronous condenser installation required two medium size condensers so that the maintenance program could be carried on and still improve, to a partial degree, the customer's low voltage. This maintenance schedule is very interesting, and I will touch upon it only briefly. One 24-hour shutdown would be required once a year, and in addition an 8-day consecutive shutdown would also be required once a year. This may seem extreme to some of you, but our experience on large rotating equipment in steel mills indicated that this would be required.

Our management was not hard to convince, but did ask if this was the least overall expensive way of doing the job, and we had comparative figures ready to show that it was.

Another interesting installation of a series capacitor application for eliminating voltage fluctuation has just been made by the Lake Superior District Power Company, in connection with mine hoists. This was a 3,000-kva installation on a 33-kv line, and it is in the Gogebic Iron Mining Range in northern Wisconsin. This is described in the January 1948 issue of *Electric Light and Power*.

We feel very proud of our installation, and while we carried on negotiations ahead of time and consulted with all and sundry over the country who could help us, including Westinghouse and General Electric, the proof of the pudding is in the eating, and Figure 3 shows the corrective effect of the voltage schematically, and Figures 2 and 4 show the actual voltage chart before and after the series capacitor was installed.

However, I would like to say a word of caution here. Make a thorough investigation of your conditions, consult all who can help you, and read up all the literature you can find on the subject, including those

references listed at the back of the paper, and I believe that you can solve some of your voltage problems by properly applying series capacitors.

Starr questioned the need for any resistor shunting the capacitor, particularly if the protective devices were allowed to flashover whenever the motor did self-excite and no damage resulted to the protective devices. The probabilities are that the motor would not self-excite at every start but only at such times as the remainder of the plant load is very low. We have considered that if the protective devices were allowed to operate in lieu of having a shunting resistor, more frequent inspection of the capacitor overvoltage protective devices would be required, and fuses on the individual capacitor units might be weakened by frequent discharge of current through them. Furthermore, damage might be done to the motor should it be subjected on starting to frequent self-excitation, which causes the capacitor by-pass gaps to flashover.

The installation is now operating with a 400-ohm shunting resistor. We hope to make further tests in the near future to determine just how much higher we can raise this resistance. There are appreciable losses in this resistor being in service continuously, and the higher we raise this ohmic value, the less the losses are, and naturally we are interested in saving the company money wherever we can. (These comments also apply to Butler's discussion.)

We have laid out the installation so that circuit breakers for switching the resistor in and out can be installed in the future, but we feel now that the cost of the losses does not justify this installation, even at a 400-ohmic setting. The cost of the losses would have to increase very appreciably before such an installation could be justified.

Relative to Starr's comments on such an installation being applicable only where lagging power factor exists, we realized this. We also realized that the corrective effect diminishes as the lagging power factor approaches unity.

Darling's comments on a 780-kva 3-phase series capacitor in a 34.5-kv line in South America in 1939, for taking out some of the flicker caused by large electric furnaces—one 800 kva and two 2,000 kva—were very interesting. Here is another case where the series capacitor really has a field.

The reason for this installation was to take the flicker out of the residential lighting, whereas in our case that was secondary, and the main reason was to raise the voltage at a large industrial customer, as well as to reduce the flicker to this customer and elsewhere on this line, so that the improved voltage would be suitable for residential lighting

Butler's discussion was gratifying to us, and his experience over a number of years with this problem indicates to him that the increased distortions appear long before the unstable run-away self-excitation is obtained. Fortunately, we did not encounter any excessive vibration during these tests, but there was some slight vibration which, we feel, may have been caused by the conditions of the water rheostat in the secondary of the 4,000-horsepower motor, which conditions are being corrected by the customer

We were very glad to note that Butler feels so confident that a very high value of resistance is tremendously more effective than infinite resistance, and we hope soon to increase this 400-ohmic resistance to a higher value, although we are treading cautiously in this matter.

While Olmsted and I are employees of the same company, his comments relative to the operation of the series capacitor and associated apparatus are very apropos, even though it had been in service only a few months. We are always trying to reduce costs of capital investment, as well as operating expenses.

We are hoping to have very soon a visible device of some kind on this installation to indicate whether the by-pass switches have operated, and the approximate conditions of the individual capacitor fuses, all without having to take the capacitor out of service, as this is very undesirable. Subsequent to the presentation of this article, encouraging progress has been made on this problem.

Design and Layout of 66-Kv 10,000-Kva Series Capacitor Substation

G. B. MILLER
MEMBER AIEE

Synopsis: This paper describes the design and layout problems encountered in the construction of a substation for the largest series capacitor installation in operation to date. Companion papers[1,2] cover the application and equipment design of the series capacitor.

AT KENNEDY SUBSTATION, in October 1947, the largest series capacitor installation in existence and the first to be installed at 66 kv, was placed in commercial operation on the Duquesne Light Company's system, Pittsburgh, Pa. The substation was installed in the 14 mile Phillips–Crucible 66-kv transmission line. This line is of modern wood pole construction and was installed in 1940 to serve a large steel plant. The initial load was about 10,000 kva, and has since increased to approximately 40,000 kva. Electric arc furnaces are the principal load. Other major load consists of large synchronous motor generator sets and a 4,000 horsepower wound rotor motor.

Until recently the only load on this line was that of the steel plant. In 1947 a tap was connected to provide 10,000 kva service for the town of Midland, Pa. This increased load, plus a large proposed extension of the steel plant, made it imperative that some action be taken to improve voltage regulation and alleviate annoying voltage flicker produced by the electric arc furnaces. The flicker also affected the stable operation of the synchronous motor generator sets. Various means of correcting this voltage condition were considered and after careful analysis it was decided to install a series capacitor. Figure 1 shows the relative location of the capacitor in the transmission line. Figure 2 shows a schematic single line diagram of the substation.

Brief Description of Equipment

The series capacitor is composed of the capacitor proper, a shunting resistor, and a grounding switch. The resistor is connected in parallel with the capacitor to prevent low frequency resonance conditions when starting the 4,000 horsepower wound rotor motor at minimum plant loads. The resistor will also prevent, or damp out, a high inrush of low-frequency current which may exist when energizing the customer's unloaded transformer banks. The grounding switch is for discharging and grounding the series capacitor when taking it out of service.

The nominal current rating of the transmission line, in which the series capacitor is inserted, is 500 amperes. The capacitor was purchased to match this current rating.

The general characteristics of the equipment are

Capacitor

10,000 kva, three phase, 60 cycles
500 amperes continuous
750 amperes for 5 minutes
675 amperes for 30 minutes
Ohms to neutral: 14.4 + 0 per cent − 13 per cent, in accordance with National Electrical Manufacturers Association Standards
The nominal rating is 13.5 ohms.

Resistor

Composed of three single-phase units each having four–400 ohm sections. Provision is made for connecting at 100, 133, 200 and 400 ohms, with continuous current carrying capacities of 72, 54, 36 and 18 amperes respectively.

Each phase of the capacitor is contained in an outdoor weatherproof steel housing approximately 28 by 4 feet, by 12 feet high. Shipping weight, less capacitors, was 11,000 pounds. The installed weight is 25,000 pounds.

Each resistor is contained in an outdoor weatherproof aluminum housing approximately 20 by 4 feet by 10 feet high. It was shipped completely assembled and weighs 9,000 pounds.

Capacitor Details

The capacitor consists of three major units, one per phase. Each phase unit consists of 240–2,400 volt, 15-kva capacitors, filled with Askarel, each with its own protective fuse. Each unit is arranged in three groups of 80 capacitors connected in parallel with the three groups connected in series. On the basis of 13.5 nominal ohms, the assembly represents an in-service capacitance of 197 microfarads and produces 6,750 volts drop at 500 amperes.

Each group of 80 capacitors is protected by a heavy-hooded graphite safety gap, which is automatically shunted out of service, in case of discharge caused by overvoltage across the capacitor bank, by means of a self-actuating contactor operated by a thermally actuated vapor-filled bellows. This protective equipment is mounted inside the capacitor housing.

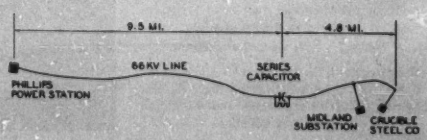

Figure 1. Location of capacitor in line

Paper 48-56, recommended by the AIEE transmission and distribution and substation committees and approved by the AIEE technical program committee for presentation at the AIEE winter general meeting, Pittsburgh, Pa., January 26–30, 1948. Manuscript submitted November 10, 1947; made available for printing December 19, 1947.

G. B. MILLER is senior engineer in the electrical engineering department of the Duquesne Light Company, Pittsburgh, Pa.

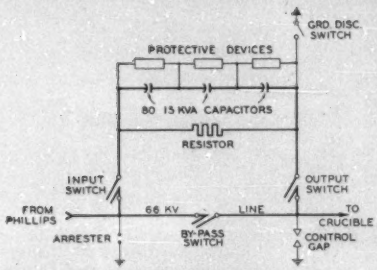

Figure 2. Schematic single line diagram

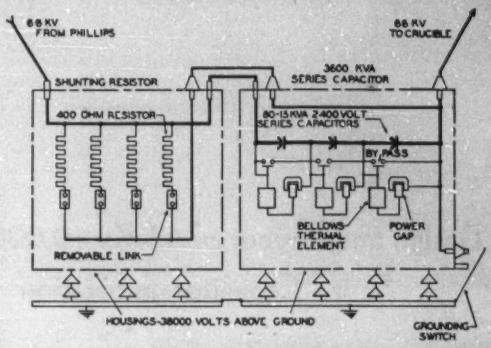

Figure 3 (right). Schematic longitudinal section of phase unit

The capacitor cubicles are mounted on 69-kv post insulators and operate at 38,000 volts above ground. Fifteen kv roof bushings are provided for line connection.

Shunting Resistor Details

The resistor housing in general matches the appearance of the capacitor, with the exception that it has a galvanized steel framework with aluminum sides. Aluminum was provided to eliminate heat damage to the housing. It is open at the top and bottom to provide adequate ventilation. The openings are covered with screens to keep out rodents and birds.

The resistor cubicles are mounted on 69-kv post insulators and are equipped with 15-kv bushings for connection to the line.

Each resistor phase unit is designed to operate continuously with 7,200 volts impressed across it. It consists of resistor groupings that can be connected at various values of resistance from 100 to 400 ohms. At present the resistor is connected for the 400-ohm value. By minor internal changes it will be possible to reconnect the resistor for higher values, up to 1,600 ohms maximum, if it is later decided to operate in excess of the 400-ohm value.

Each phase unit of a capacitor and shunting resistor was installed in line, and about four feet apart, on a flat monolithic concrete foundation about 50 by 5 feet by one foot thick. Piers extending two feet above the foundation were installed to

Figure 4. Capacitor and resistor viewed from the end of the resistor

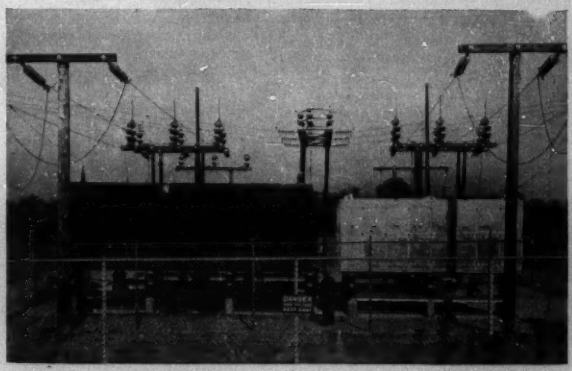

Figure 5. Side view of equipment

The capacitor is on the left, the resistor on the right. The input switch is on the right, the by-pass switch in the center, and the output switch on the left

Figure 6 (above left). Capacitor protective compartment

Figure 7 (above right). Interior view of capacitor housing

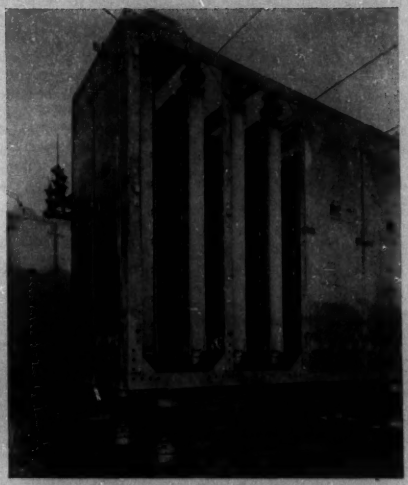

Figure 8. Interior view of resistor housing

elevate the equipment to about maximum winter snow level height, and also to prevent weeds from short-circuiting the 69-kv supporting insulators.

Figure 3 shows a schematic arrangement of the resistor and capacitor housing. Figures 4 to 8 show this equipment.

Grounding Switch

To effectively discharge the capacitors after isolating them from the line, a 3-pole 69-kv remotely operated shunting disconnecting switch was installed. When closed, it shunts the capacitor and resistor, and grounds their housings. Its operation is controlled from outside the fence. The switch and operating mechanism is shown in Figure 9.

Substation Site and Arrangement of Equipment

The substation is located on 1.4 acres of gently sloping farm land, adjacent to an improved highway, 4.8 miles from the customer's end of the transmission line. The site lies directly under the transmission line. Figures 10 and 11 show a schematic layout and general view of the substation.

The series capacitor phase units are located on 15-foot centers. The concrete foundation slabs supporting them are unique, as no piers or footers are used below the frost line. These slabs are at ground surface elevation and set on nine inches of crushed slag. This construction allows for expansion of the ground due to freezing and will permit seasonal movement of the equipment. This, however, is not considered to be objectionable as each phase unit is entirely independent of the others and free to move.

No provision was made for station service. However, a distribution line runs nearby and could be made available if it is required later. There is no apparent need for it, as there are no relays, control, light or heating.

Switching Equipment

The 66-kv switching equipment required for placing the equipment into or taking it out of service consists of three 3-pole 600-ampere 69-kv air break disconnecting switches.

At the inception of this job it was thought the main by-pass switch would have to be a 69-kv oil circuit breaker of 1,000,000 or 1,500,000 kva interrupting

Figure 9. Ground switch in closed position

Note the operation of switch has permitted the gate at the left to open

capacity. However, before deciding on this, a factory laboratory test was made to determine if the charging current to be encountered could be safely broken with a horn gap air break switch.

These tests consisted of opening and closing a 15-kv air break disconnecting switch in a 20-kv test circuit with a series capacitor connected in shunt with it. The series capacitor consisted of 80–15-kva 2,400-volt capacitors connected in parallel. This arrangement of capacitors will pass 500 amperes with 2,400 volts impressed across it. The arcing horns of this switch, after twenty operations, did not show excessive burning.

These tests were performed at the same current value, with equipment and line voltages at approximately one-third of that proposed for the 10,000-kva series capacitor. The result of these tests indicated that a 69-kv air break switch would be satisfactory.

The full complement of switches—that is, the by-pass switch, the input switch, and the output switch were installed on wood pole structures approximately 28 feet high. They are operated manually from geared operating handles located near the ground. To facilitate maintenance of the switches without circuit interruptions, hot-line connectors were installed at the switch terminals.

During the course of test operations, conducted prior to cut-in, these switches were operated several times. All of the operations were very satisfactory, with the exception of the first time the by-pass switch was opened slowly with the capacitor in service. The result was a rather thick arc, which was drawn out from 12 to 15 inches in length before it blew out. The load current at this instant, so far as could be ascertained, was approximately 360 amperes. After this the switches were opened more rapidly and the amount of arcing on the by-pass switch was considerably less. There was no arcing at any time on the input switch. The arcing on the output switch never exceeded six to eight inches in length and was very thin.

These operations demonstrated that air-break switches are adequate to break charging currents of a capacitor bank of this size. Therefore, a considerable amount of investment was saved by not having to install a circuit breaker in the by-pass switch position.

Safety to Personnel

Safety to personnel is provided by:

1. Enclosing the capacitors and resistors inside a fence. This fence is 25 feet inside the one enclosing the entire installation. Both are 10 feet high with barbed wire at the top.
2. Key interlocks insuring the proper sequence of disconnecting switch operation.
3. A mechanical gate interlock which prevents anyone from entering the inner fence, where the capacitors and resistors are located, before the grounding switch is closed.
4. Grounding grid of low resistance.

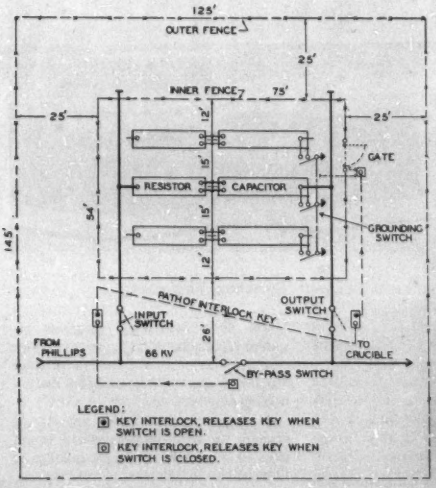

Figure 10 (left) Schematic layout of station. Note path of interlock keys

Figure 11 (below). General view of substation looking toward customer's plant

The lightning arresters are on the structure at the right, the control gaps in the right background. Note arrangement of overhead ground wires on tall poles

Figure 12 (left). Mechanical gate interlock in open position

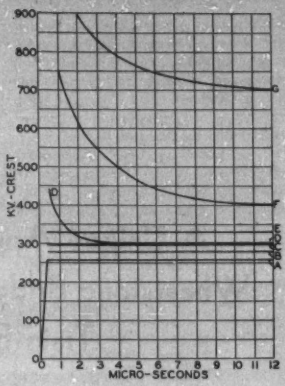

Figure 13. Impulse insulation curves

A—73-kv lightning arrester — 1,500 amperes
B—73-kv lightning arrester — 3,000 amperes
C—73-kv lightning arrester — 5,000 amperes
D—Control gap—11 inch setting
E—73-kv lightning arrester — 10,000 amperes
F—Equipment insulation—69-kv class
G—7–10 inch suspension insulators on station bus

The schematic layout shown in Figure 10 embodies the first three foregoing items. Figure 12 shows the mechanical gate interlock.

Ground Resistance

An attempt was made in the design of the grounding grid to limit the ground resistance to approximately one ohm. Sample ground resistance readings, taken at the beginning of construction, indicated that a grounding grid constructed of a reasonable number of ground rods would probably develop a resistance of 2.3 ohms. The resistance of the transmission line grounding grid taken on the same day was also 2.3 ohms.

The two grounding grids were tied together to produce low ground resistance. It was expected that 1.15 ohms would be produced by the combination. Actually, the resistance obtained was 0.65 ohm and somewhat better than anticipated. This reading was taken during wet weather. Dry season conditions probably will give a higher resistance, however, it is expected the value will not exceed the one ohm figure.

Station Impulse Level

The 66-kv transmission line being of wood construction, with insulated wood guys and grounded overhead shielding wires, has impulse insulation far in excess of the station equipment insulation. In order to protect the station equipment from impulses or surges entering the station, both lightning arresters and control gaps have been installed. In addition, overhead shielding wires have been installed over the equipment area and tied into the transmission line shielding wires. These shielding wires can be clearly seen at the top of the four high poles in Figure 11.

The arresters are rated at 73 kv and are of the line type porous-block construction, consisting of three 25-kv units in series. The control gap electrodes are set at 11-inch spacing. All station equipment, including the control gaps and disconnecting switches, have 69-kv heavy-duty station type insulators.

Figure 13 shows the various impulse levels of the station insulation. The arresters should operate first. In event they fail, the control gaps arc over and provide back-up impulse protection to the station equipment insulators. The arcing over of the gaps will cause the line to be tripped out on short circuit back at the Phillips station. Seven 10-inch line type suspension insulators are installed in each insulator string at the substation, as compared to six insulators plus five feet of effective wood on the transmission line.

Continuity Tests

To safeguard the possibility of installing any defective capacitor units, the entire number, 720 in all, were tested in the company's laboratory at 75 per cent of the standard National Electrical Manufacturers Association test. Several were found defective, indicating that this additional precaution was worth while.

After the capacitors were installed in the field, the individual fuses were checked for continuity. Only one or two defective ones were found. An over-all continuity test of each phase unit was made prior to being cut-in. A gasoline engine generator was used for this purpose. Measurement at 60-cycles 115-volts showed the capacitive reactance to range from a minimum of 13.3 to a maximum of 13.6 ohms, or very near the expected nominal value of 13.5 ohms.

The safety gaps which protect the capacitors from overvoltage were checked and set in accordance with the manufacturer's recommendation.

A continuity test was also given the shunting resistor prior to cut-in.

References

1. A 10,000-Kva Series Capacitor Improves Voltage in 66-Kv Line Supplying Large Electric Furnace Load, B. M. Jones, J. M. Arthur, C. M. Stearns, A. A. Johnson. AIEE Transactions, volume 67, 1948, pages 345–54.

2. Design and Protection of 10,000-Kva Series Capacitor for 66-Kv Transmission Line, A. A. Johnson, R. E. Marbury, J. M. Arthur. AIEE Transactions, volume 67, 1948, pages 363–8.

No Discussion

A Unique Application of the Wheatstone Bridge to High-Speed Train Braking

C. M. HINES
MEMBER AIEE

Synopsis: This paper describes a method for indicating to the locomotive engineman the condition of the circuits controlling the electropneumatic brakes which are now in widespread use on high-speed passenger trains. The apparatus consists of a Wheatstone bridge so arranged as to insure the operation of the bridge itself as well as to indicate the condition of the electropneumatic brake circuits. Calibrated dials indicate the number of vehicles in the train. This equipment is proving to be a useful tool in making terminal tests, and assist in determining the location of any change of resistance of the train line wire circuits which may occur. It also provides a convenient means for indicating to the engineman while en route, any change which may occur in the train line circuits. All parts of the equipment are designed to provide the ruggedness essential for long life on locomotives.

THE general public is no doubt unaware of the fact that the pneumatic brakes on most high-speed trains are normally controlled electrically with pneumatic control standing by in reserve. The term high-speed trains as used here applies to passenger trains running at speeds in excess of seventy-five miles per hour. The advantages of this type of electrical control have been recognized for many years, the first tests of such a brake being made in 1887. General acceptance did not occur until recent years, the modern form of the electropneumatic brake having been first applied to the early zephyr trains in 1933. A full description of this type of brake and the advantages accruing from its use, are covered in a previous paper.[1]

Since 1933 most high-speed trains have been equipped with electropneumatic brakes. Figure 1 indicates the essential elements of the present electropneumatic brake system. A pneumatically-operated master controller is equipped with contacts which supply energy by means of train line wires to application and release magnet valves, one pair being on each vehicle. It will be noted that all application magnets are in parallel and all release magnets are in parallel, a common return being used for both sets of magnets. When an electropneumatic brake application is made, movement of the brake valve handle introduces air under pressure to the chamber on the left side of the master controller, Figure 1, the pressure being proportional to the angular movement of the brake valve handle. The diaphragm of this chamber moves the lever controlling the contacts to the right, closing both contacts, and thus energizing both the release and application magnets throughout the train. Energizing the release magnets results in closing off the brake cylinders from atmosphere. Energizing the application magnets causes air under pressure to be admitted to the brake cylinders. At the same time, air at corresponding pressure is admitted from every vehicle to a pipe called the straight air pipe. This pipe is connnected to the right-hand chamber of the master controller. When the pressure in the straight air pipe and the chamber becomes equal to the pressure in the left-hand chamber, the lever moves to the left, but only a sufficient amount to open the contact which controls the application magnets. This halts the build-up of brake-cyclinder pressure at the value demanded by the position of the brake valve handle. The magnet valves control brake cyclinder pressure indirectly by means of a relay valve as shown in Figure 1.

With the preceding explanation of the circuits involved in the present electropneumatic brake system, it becomes apparent that it is desirable to have some means for indicating to the engineman any change which may take place in the condition of the circuits controlling the magnet valves throughout the train. The usual automatic air brake is always

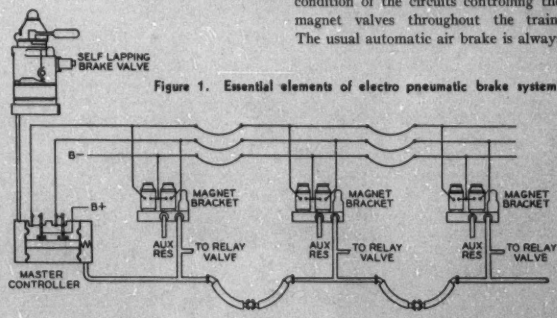

Figure 1. Essential elements of electro pneumatic brake system

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C. M. Hines is electrical engineer with the Westinghouse Air Brake Company, Wilmerding, Pa.

The author wishes to acknowledge the work on bridge circuits in hot box detectors done by G. W. Baugham (M '45) and A. B. Miller of the Union Switch and Signal Company, Swissvale, Pa.

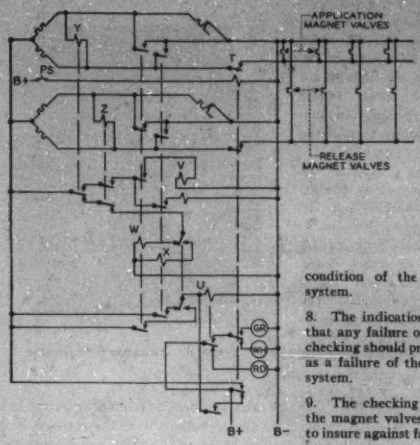

Figure 2. Schematic diagram of circuit checking equipment

T—Control relay
Y, Z—Detector relays
U, W, X—Time delay relays
V—Polarity relay
GR—Green light
WH—White light
RD—Red light
PS—Pneumatic switch

available and may be made operative by moving a lever on the brake valve. If sufficient time is not available to shift this change-over lever, an emergency brake is available simply by moving the brake valve handle to its extreme applied position. Since the usual form of brake application requires only a service application, practical operating experience indicates the desirability of informing the engineman of any change in the condition of the electropneumatic brake circuits, in order that in advance of a brake application he may change to the automatic air brake, and thus avoid the use of the emergency brake.

In considering a means for thus indicating the condition of the electropneumatic brake circuits the following tentative requirements were made as presenting an ideal solution:

1. All equipment for checking should be on the locomotive.

2. Because of the large number of passenger cars now in service with the electropneumatic brake, no change in car wiring should be required.

3. The equipment must be designed to operate satisfactorily with trains varying in length from 1 to 24 vehicles.

4. The equipment must operate satisfactorily over a wide range of voltage, the limits being from 58 to 80 volts.

5. The equipment must be sufficiently rugged to operate satisfactorily on a locomotive, long life and a minimum of maintenance being essential.

6. No power supply other than that used on present locomotives should be required.

7. The equipment must indicate continuously in advance of a brake application the condition of the electropneumatic brake system.

8. The indication means must be safe in that any failure of the equipment used for checking should provide the same indication as a failure of the electropneumatic brake system.

9. The checking current flowing through the magnet valves must be sufficiently low to insure against false operation.

With these requirements in mind, a system was devised which fulfills them. After making tests with numerous methods, it was finally decided that the use of the Wheatstone bridge was the solution. This old standby presents a ready means for accurately measuring the resistance of the magnet valves and the train line wires which control them, and so could be used to indicate any change in resistance resulting from open circuits or short circuits. The disadvantage of using the Wheatstone bridge lies in the fact that a null indication represents a balanced condition. Therefore, a fault such as an open lead to the galvanometer in the Wheatstone bridge would indicate a balanced condition, and a fault in the brake system would not be detected.

One unique feature of the system finally adopted lies in the means for converting the Wheatstone bridge into a device which checks its own operation as well as indicating any change in the condition of the circuits whose resistances are being measured. Figure 2 shows schematically the circuits involved in the system and all devices are shown in the de-energized position, it being assumed that the battery supply is open.

Two bridges are used, one for checking the application wire circuits and the other for checking the release wire circuits. The galvanometer usually associated with the Wheatstone bridge is replaced by a relay. This relay does not have to be a delicate instrument as will be shown presently. In addition to detector relays Y and Z, three additional relays are used for controlling the bridges. Relays W and X are used to alternately short circuit one of two arms of each bridge. The third relay V is of the polarized type in that the position of its contacts is controlled by the direction of current flow through the coils. The contacts of this relay control the W and X relays to select which one is operated. The sixth relay in the system, relay T, is used to disconnect the circuit checking equipment from the application and release wires during a brake application and to indicate that the brakes are applied. The seventh relay U controls the light indication or other means to warn the engineman of faulty circuits.

The sequence of operations is as follows: one of the time-delay relays, W or X, is first energized, short circuiting one arm of each bridge, and thus providing a large degree of unbalance to insure energizing detector relays Y and Z. The time-delay relay then becomes de-energized, and the detector relays can release, provided there is no unbalance of the bridges sufficient to hold either relay energized. This completes one half of the operating cycle. The polarity relay V is controlled by the action of the detector and time-delay relays, and its contacts select which of the two time-delay relays is to be energized. If during the first half cycle the

Figure 3. Relay cabinet

W relay is energized, thus placing a short circuit across the upper-right hand arm of each bridge, then during the second half cycle the X relay becomes energized, placing a short circuit across the lower right-hand arm of each bridge. It can be followed readily that if at any time a change in resistance takes place, whether it be a decrease or an increase, the Y or Z relays, or both, will be held energized when the short circuit is removed from the arms of the bridge, provided the change in resistance is such that the direction of current flow through the detector relays is not changed. This may occur during either the first or the last half of the complete cycle depending on the direction of unbalance. It can also be seen that the amount of unbalance caused by the change in resistance need be great enough only to hold the detector relay energized. The detector relays Y and Z as explained previously, may be rugged devices and they are particularly designed to have a low drop out with respect to the pick-up current.

Sensitivity to change in resistance can be made as desired. For the type of service being described, it is felt that detection of changes of 10 per cent are adequate, and that higher degrees of sensitivity would result in unnecessarily switching from the electropneumatic brake to the automatic brake with the consequent loss of the advantages of the former system. Therefore, the components of the circuit checking equipment have been designed to indicate a change in resistance of 10 per cent.

As relays W and X are alternately energized, relay U receives pulses of current to hold it energized, it being a time-delay device so as to bridge the time between pulses. If for any reason these pulses cease, relay U will become de-energized and the green indication normally seen will be changed to red.

If a brake application is made, the white light will become energized, provided the electropneumatic brake circuits, as indicated by the checking circuit equipment, had previously been intact. If a brake application were made with the red indication showing, the red light would continue to be energized.

The relay cabinet housing the control equipment is shown in Figure 3. Calibrated dials are used with the adjustable rheostats which form the upper right-hand arm of each bridge and serve to indicate the number of vehicles in the train. Since most of the faults occur in the jumper connections between cars, these calibrated dials serve a further purpose in that they are of considerable aid in locating the source of trouble. Figure 4 shows the light-indicating unit mounted in the locomotive cab within view of the engineman.

When initially taking control of the train, the engineman makes an electropneumatic brake application and the train is inspected to insure that brakes apply properly throughout. This is an indication that the electropneumatic brake wiring is intact and functioning as intended. The number of vehicles in the train must be known to the engineman. If the train consisted of three locomotive units and twelve cars, a total of fifteen vehicles would be used in setting the rheostats. The dials of the rheostat knobs are calibrated from one to twenty-four vehicles. The engineman, after determining that the electropneumatic brake is functioning properly and knowing the number of vehicles in the train, can then proceed to place the circuit-checking equipment in operation. The on-off switch to the left of the lights must be moved to the "on" position and both rheostats should be turned so that the proper number of vehicles on the dial is opposite the indicator for each rheostat. With both rheostat dials so set, the three-position toggle switch to the right of the lights is moved and held to the "up" position. This prevents the pulsing action of the relays and at the same time inserts a milliammeter in series with the corresponding detector relay. The power switch and three-position toggle switch are omitted from Figure 2 in order to simplify the circuits. The rheostats should then be readjusted until each milliammeter reads zero. This insures that both bridges are balanced. The three-position switch should then be pulled to the "down" position until the green light is energized, after which it may be released and it will automatically assume the center position. The calibration of the rheostat dials is merely an approximation and the milliammeters are used to insure accurate balancing of the two bridge systems. Nevertheless, the calibration on the dials will serve to indicate to a rather close extent the number of vehicles in the train, and if there is any large variation between the dial settings and the actual number of vehicles in the train, a fault in the train wire system is indicated.

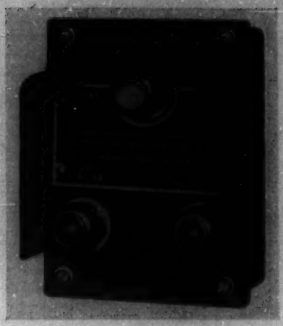

Figure 4. Indicating light panel

To locate such a fault let us assume that in the 15-vehicle train the rheostat dial must be set to 12 to obtain a balance for the application wire. This indicates a faulty connection between the 12th and 13th vehicle, and that the poor connection must be in the application wire. If both rheostats register 12 vehicles, instead of 15, the fault might be in both the application and release wire, in the common return wire, or in all three. This means of fault location is applicable only if the fault is an open circuit existing in the plug connectors or jumper wires between cars. Experience has proved that most faults in the electropneumatic brake system are of this nature and consequently this method of fault location may be of considerable practical value from the standpoint of maintenance.

No attempt has been made to cover fully the circuit checking equipment in detail. A number of refinements vital to this particular application would be of little general interest. However, it is believed that the preceding information is insufficiently clear to give a general understanding of a new method for indicating change from a balanced condition. It can be used with any type of bridge circuit. The method is one which checks its own integrity, and which lends itself to applications demanding rugged apparatus, long life, and a minimum of maintenance.

Reference

1. Electropneumatic Brakes for High Speed Trains with Particular Reference to Their Electrical Features, Joseph C. McCune. AIEE Transactions, volume 61, 1942, March section, pages 137–41.

No Discussion

Design and Protection of 10,000-Kva Series Capacitor for 66-Kv Transmission Line

A. A. JOHNSON
MEMBER AIEE

R. E. MARBURY
FELLOW AIEE

J. M. ARTHUR
ASSOCIATE AIEE

Synopsis: To correct for voltage regulation and light flicker on the 66-kv line supplying arc furnace and steel plant load, a series capacitor of 500 amperes continuous rating and 13.5 ohms was installed. This series capacitor was built with three groups of standard 2,400-volt 15-kva capacitor units connected in series per phase with each group being protected by its own by-pass gap and automatic switch. This is the largest and the highest voltage series capacitor so far installed.

THE Duquesne Light Company supplies the Crucible Steel Company at Midland, Pa., with electric service over a 66-kv 3-phase 60-cycle transmission line from the Phillips Power Station. To correct for undesirable voltage regulation and excessive voltage flicker caused by arc furnace load at the steel plant, the Duquesne Light Company decided that series capacitors would be applied to the 66-kv transmission line. Series capacitors are fundamentally fitted for this type of application because they have inherently instantaneous correction in reducing the range of voltage regulation and reducing light flicker. A series capacitor of 14 ohms having a normal current rating of 500 amperes was found to provide the necessary correction.

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A. A. JOHNSON is central station engineer, R. E. MARBURY, manager, capacitor section, switchgear control engineering department, both with the Westinghouse Electric Corporation, East Pittsburgh, Pa. J. M. ARTHUR is development engineer with the Duquesne Light Company, Pittsburgh, Pa.

To obtain the 500-ampere current rating and the required ohms, it was decided that standard 2,400-volt 15-kva capacitor units would be used and properly grouped. Using these standard units requires three groups in series per phase to obtain the desired ohms with a sufficient number of units in parallel to obtain the current carrying capacity of 500 amperes. By using a sufficient number of standard 2,400-volt units to obtain the 500-ampere continuous current rating per phase, the nominal ohms per phase would be 14.4.

Rating of Series Capacitor

Several factors have to be considered in determining the rating of a series capacitor.

1. Maximum continuous working current—500 amperes (except following a period during which the capacitor has been out of service when the ambient temperature has been below 0 degree centigrade, in which case, the capacitor current should not exceed 350 amperes until the capacitor warms up to 0 degree centigrade). The time required for the capacitor reactance to return to normal after being at temperatures below 0 degree centigrade is given in the following tabulation:

Initial Capacitor Temperature	Time in Minutes
−10 degrees centigrade	5
−20 degrees centigrade	30
−30 degrees centigrade	104

2. Maximum short time current.

Current	Time
500 amperes	Continuous
675 amperes	30 minutes
750 amperes	5 minutes

Short time overloads which may occur during normal operation of the series capacitor should not exceed 750 amperes, as currents greater than 750 amperes will approach the zone of operation of the short circuit protective device which is set for a nominal voltage corresponding to approximately twice the continuous current rating. However, overloads between 500 and 750 amperes may be permitted for the times given in the foregoing tabulation.

3. Maximum permissible fault current—5,000 amperes. This limitation is determined by the protective devices.

4. Capacitance ohms—nominal 14.4 minus a maximum of 13 per cent, actual ohms 13.5.

5. Line voltage—66 kv.

6. Rating of individual capacitor units—2,400 volts.

7. Rating of individual capacitor units—15 kva.

8. Line frequency—60 cycles.

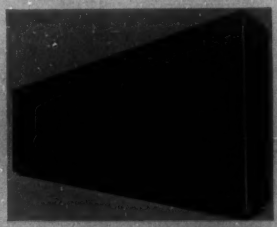

Figure 1. Series capacitor housing for one phase ready for shipping

Design of the Series Capacitor

The capacitor equipment for each phase of the 66-kv circuit consists of a structural steel housing 28 feet long, $9^1/_2$ feet high, and $3^1/_2$ feet wide. This housing has 240 15-kva 2,400-volt type *FP* Inerteen capacitor units. The housing has three tiers, each containing 80 capacitor units. The 80 units on each tier are connected in parallel with individual fuses for each capacitor. The

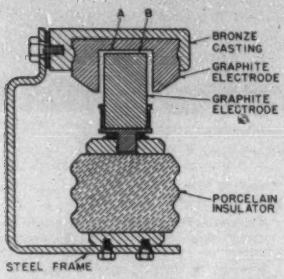

Figure 2. Special gap for protecting series capacitors

three groups of 80 units are connected in series to form the series capacitor for one phase of the 66-kv circuit.

Each housing containing 240 capacitor units is supported on eight 69-kv post-type insulators. One insulated roof bushing provides for the 7,200-volt insulation while the housing forms the other terminal. The insulation to ground for the housing and all of the series capacitor units is provided by the 69-kv insulators between the housing and ground.

A grounding switch is provided for shorting the series capacitor and grounding it so as to protect the operator from charges on the capacitor and accumulated static charges on the housing. This grounding switch is interlocked with the main by-pass and the isolating disconnect switches. These devices are discussed in a companion paper.[2]

Design of Capacitor and Protective Device Housing

The outdoor series capacitor equipment consists of the capacitor compartments together with a protective device compartment assembled on a structural steel base and covered with a formed-sheet roof with provision for roof entrance bushings. Flexible braided connectors and horizontal and vertical bus bars are used for making connections between capacitors, protective devices, and roof entrance bushings.

The capacitor compartments and the protective device compartments including the doors are constructed of structural steel angles and formed sections of 1/8 inch thick steel welded together to form a rigid and self-supporting structure. The doors have concealed hinges and are provided on each side of the housing to permit ready access to the capacitor units and the individual capacitor fuses. The capacitor compartments have three shelves each arranged for supporting two rows of indoor capacitor units. The bottom of each capacitor compartment is covered with perforated sheet steel, and the side walls near the roof have screened ventilators to provide adequate ventilation. Figure 1 shows the complete series capacitor housing for one phase ready for installation.

Protection of Series Capacitor Against Overvoltage Accompanying Line Faults

Gaps

The voltage rating of the series capacitor was determined by the required voltage correction on the basis of the continuous working current of 500 amperes and the use of standard 2,400-volt units, as mentioned previously. This permits a maximum current loading of 1.5 times rated current or 750 amperes for 5 minutes to take care of short time overloads and the application of a parallel gap for voltage protection set above the voltage corresponding to 1.5 times rated current. The parallel gap protection is provided for each of the three groups of 80 capacitor units and limits the voltage which may appear across the series capacitor to approximately double the continuous rating of each group or about 4,800 volts.

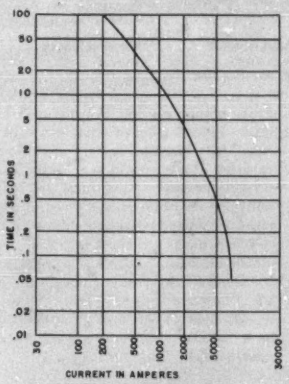

Figure 3. Time-current curve of special gap based on permissible temperature rise

The gap protection consists of a gap designed for low arc drop and is designed in such a manner that arcing can take place without restriking, thus effectively protecting the capacitor if the gap has broken down. The flow of current through the gap also passes through a thermal element which initiates the closing of a switch which by-passes both the gap and the capacitor units, thus limiting the heating of the gap. Once the gap is by-passed by the switch, current ceases to flow through the thermal element and its temperature returns to normal. As the thermal element cools off, the by-pass switch is opened and the series capacitor is restored to service.

The gap is shown in Figure 2 and consists of a recessed upper electrode thermally loaded with a heavy metal casting. The lower electrode extends into the recess and the arc takes place at the top space provided for that purpose. Figure 3 shows the time-current working range of the gap based on a permissible safe temperature rise.

By-Pass Switch for Each Gap

Initially, consideration was given to the use of a 69-kv oil circuit breaker to by-pass all of the capacitor units in all three phases in the event of a gap flashover on any phase which was sustained. Such a circuit breaker is expensive, requires extra space and maintenance and, therefore, was not a desirable solution to the

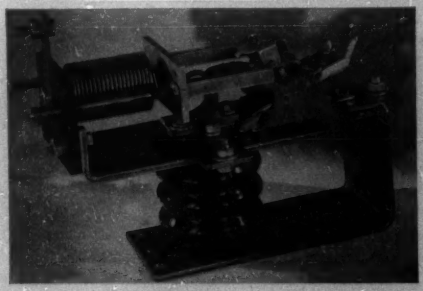

Figure 4. By-pass switch showing the contacts and the operating bellows

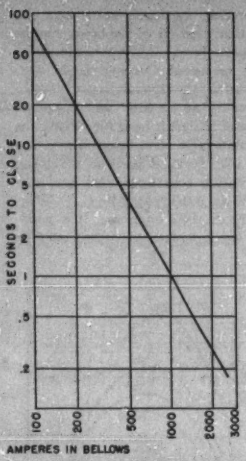

Figure 5. Time-current characteristics of the bellows-operated by-pass switch

problem. By providing a parallel gap for each of the three groups of 2,400-volt capacitors on each phase, a very simple bellows-operated by-pass switch was applied across each gap as mentioned before. Thus, the undesirable 69-kv by-pass oil circuit breaker was eliminated by applying nine small, simple, rugged switches. This scheme is applicable because high-speed restoration of the series capacitor to the circuit after a by-pass switch closure is of minor importance, particularly when it is realized that these switches will very seldom be required to operate.

A by-pass switch is shown in Figure 4. It contains a toggle mechanism actuated by a special design stainless-steel bellows containing volatile liquid sealed after the removal of air. At all ambient temperatures, the bellows contracts with a force which is able to open the switch when forced into the closed position and released. A current flow through the bellows causes the contraction force to disappear due to vapor pressure which builds up high enough to cause the bellows to expand and close the switch. The closing of the switch terminates the flow of current through the bellows but the switch remains closed until sufficient condensation takes place within the bellows to develop the required contraction force to reopen the switch. Figure 5 shows the the time-current characteristic of the switch. Figure 6 shows the scheme of operation and the approximate steady-state temperatures for opening and closing operations. The actual bellows temperature exceeds these values due to overshoot to a degree dependent on the magnitude of the current and the time lag of the mechanism.

Each group of 80 capacitor units in the series capacitor is directly protected by the parallel gap. The main purpose of the by-pass switch is to limit the duration of the arc within the gap. However, another important purpose of the by-pass switch is to restore the series capacitor to the circuit if the gap should be flashed over due to transient voltages of any kind.

The three protective devices which include the gap and the by-pass switches for the three groups of 80 capacitors per phase are placed in the front of the housing and supported on 7,500-volt insula-

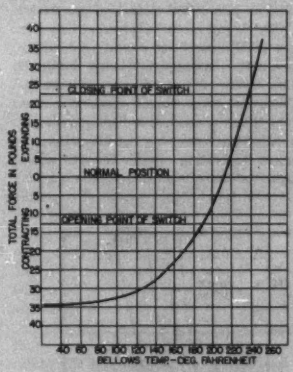

Figure 6. Scheme of operation of bellows-operated by-pass switch showing the approximate steady-state temperatures for closing and opening operations

tors. The housing for each phase is the same. The protective devices described have the advantage of requiring no external power source. The advantage of the bellows-operated by-pass switch lies in the fact that the bellows current elements are not inductive and operate over a wide range of current. Where the possible fault current is greater than 2,500 amperes, as in this case, the bellows is operated by means of a saturating current transformer which limits the current through the bellows to 2,500 amperes for fault currents in the line up to 5,000 amperes. Figure 7 shows the time-current characteristic of the current transformer and bellows switch combination. At the low current range, the current through the bellows is stepped up; and in the medium range, the ratio is about one to one. At the high currents, the bellows current is limited to a safe value for the bellows. Since each group of 80 capacitor units is protected separately by self-contained protective devices, the protective devices operate independently as required.

Protection Against Overload

Since the normal line current is well within the rated current of 500 amperes; and since the load currents are under frequent observation by operating personnel, no special provisions were made to by-pass the series capacitor in the event of prolonged overloads. Overload protection, if desired, can be obtained at the source end of the 66-kv feeder by putting in an alarm device which will indicate prolonged loading in excess of the rated current value of 500 amperes.

Failure of an Individual Capacitor Unit

Individual fuses are provided on each capacitor unit which will automatically disconnect a unit that might become short circuited internally. Figure 8 shows the time-current characteristic of each fuse. The rated current through each capacitor unit is 6.25 amperes. This type of fuse protection is ample because of the large number of capacitor units in parallel. The operation of one or two fuses will have little effect on either the voltage on the capacitors or the total reactance of the entire installation.

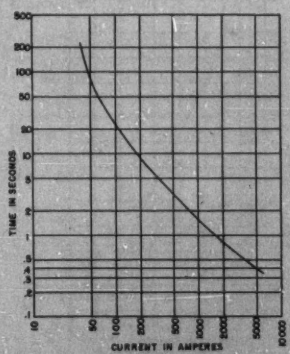

Figure 7. Time-current characteristics of the saturating current transformer and bellows-operated by-pass switch combination

Protection Against Resonance

The possibility of subsynchronous resonance under certain conditions of load and motor starting is well known in connection with series capacitors. It is, therefore, common practice to stabilize series capacitors by placing in parallel with them a resistor unit of suitable ohms. The effect of the parallel resistor is to either prevent or damp out subsynchronous resonance associated with motor starting and magnetizing current on power transformer banks. It also damps resonance at any frequency.

Each phase of the series capacitor is shunted by a resistor which may be set for several values of ohms between 100 and 400 ohms. The resistor unit has a thermal rating for continuous operation at any resistance value.

Determination of Shunt Resistor Ohms

As stated before, a series capacitor is very effective in improving voltage conditions but motor starting may cause subsynchronous resonance conditions. To prevent these undesirable operating conditions, a resistor of proper ohms in parallel with the series capacitor may be required. Such motor difficulties have been experienced on smaller series capacitor installations and are known as self-excitation where the motor rotor locks in and rotates at some speed considerably lower than normal speed. When this happens, excessive subsynchronous frequency currents will flow. The frequency of the subsynchronous current corresponds to the resonant frequency of the entire system when viewed from the rotor of the motor involved. Many factors determine whether or not a given motor will lock in at some subsynchronous speed when it is started.

1. Speed of acceleration.
2. Other loads in parallel with the motor.
3. The power factor of the parallel load.
4. The type of starting device used.
5. The WR^2 of the motor and its load.

At Crucible Steel Company, there are in operation several large induction and synchronous motors. To determine beforehand whether any or all of these motors are susceptible to self-excitation after the installation of the 10,000-kva series capacitor, an extensive study of all known theories and formulas on the phenomenon of self-excitation was made.

The largest motor involved at Crucible is a 4,000-horsepower wound rotor induction motor having a very large flywheel. This motor drives two d-c generators. The motor when started accelerates at a very slow rate and, therefore, it is questionable whether or not it will come up to proper operating speed if the series capacitor has no shunting resistor.

It became apparent, after some investigation, that all of the larger motors and especially the 4,000-horsepower induction motor at Crucible might self-excite after the installation of series capacitor if no resistor was provided. Because of the great need for solving the self-excitation problem before it occurred at Crucible, E. R. Whitehead developed self-excitation equations along lines suggested in an AIEE paper by C. F. Wagner.[4] From these equations, it is possible to tell approximately the amounts and power factor of loads needed at Crucible to prevent self-excitation of motors. It is also possible to determine the amount of shunt resistance to be placed in a parallel with the series capacitors to prevent the self-excitation of a given motor with no other load involved. This method gave for the 4,000-horsepower motor a required minimum parallel resistor of approximately 90 ohms.

The system impedance from the generated voltage on the Duquesne Light Company system to and including the 4,000-horsepower motor at the steel plant were set up on a calculating board and studied with various applied frequencies. The characteristics of the load, the motor and of the entire transmission circuit, and the magnetizing current of the trans-

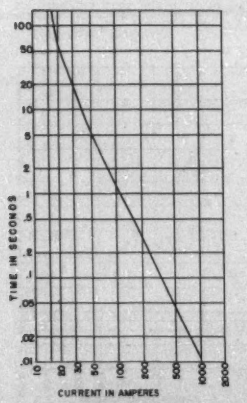

Figure 8. Time-current curve of the boric acid fuse used with each 15-kva 2,400-volt capacitor unit

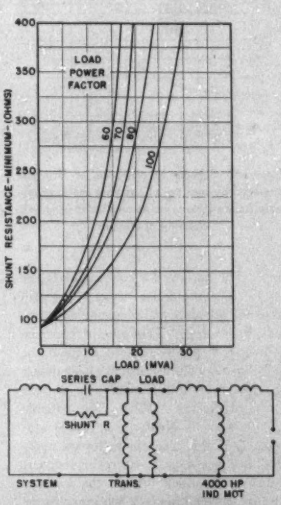

Figure 9. Minimum parallel resistance for various loads to prevent subsynchronous resonance when starting 4,000-horsepower induction motor

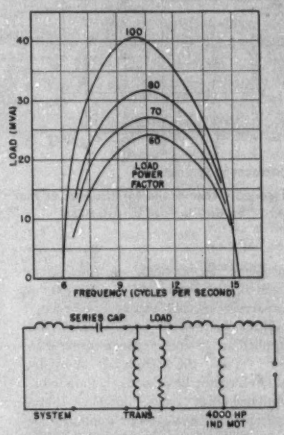

Figure 10. Calculated subsynchronous resonant frequencies which may occur when starting 4,000-horsepower induction motor

For loads outside of the curves, no resonance will take place

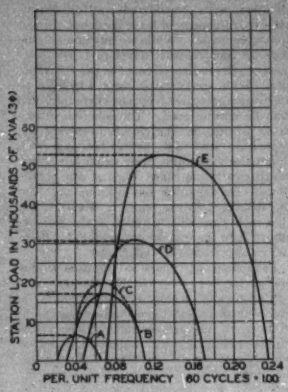

Figure 11. Crucible Steel Plant load at 100 per cent power factor to prevent self-excitation of various sizes of motors
A—500 horsepower synchronous motor
B—1,000 horsepower synchronous induction motor
C—6,000 horsepower synchronous motor with reduced voltage starting (37 per cent tap)
D—3,000 horsepower synchronous motor
E—4,000 horsepower induction motor

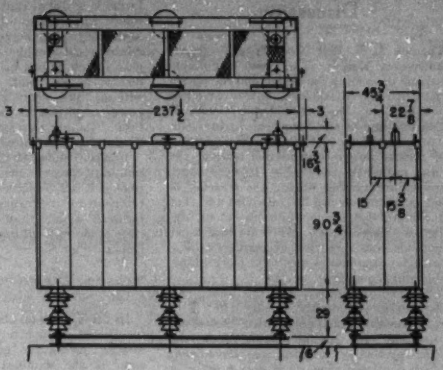

Figure 12. Resistor for operation in parallel with series capacitor

formers were considered. It was established from this study that a minimum of 90 ohms was required in parallel with the series capacitor to prevent or damp out resonance with no other load. Figure 9 gives the results of this study which checks with the theoretical calculations discussed previously. For a load of 30,000 kilovolt-amperes at a power factor of 100 per cent, a minimum resistance of 400 ohms is required in parallel with the series capacitor. As the power factor of the load decreases, a higher value of ohms in the resistor is permitted.

Figure 10 shows the range over which the circuit would be subject to subsynchronous frequencies for different loads and different power factors without a resistor in parallel with the series capacitor. It will be noted from this figure that the minimum resonant frequency with no load other than the motor on the line would be about six cycles. This figure also shows the load conditions which do not require a parallel resistor. For example, at a load of 32,000 kilovolt-amperes and 80 per cent power factor, without a resistor, no resonance will occur because this value of load lies outside of the 80 per cent power factor curve.

The results given on Figures 9 and 10 show the ideal or theoretical conditions under which this 4,000-horsepower induction motor will be subject to subsynchronous frequencies. The minimum value of ohms which was established from these studies was 90. The minimum value of ohms specified for the actual installation was 100 which was believed to be a very reasonable figure. As a matter of fact, it was expected that the system will operate satisfactorily with a parallel resistor higher than 100 ohms. A companion paper[1] shows that a resistor of 400 ohms was found to be satisfactory from tests.

Figure 11 shows the amount of 100 per cent power factor load required to prevent self-excitation on the various motors in the steel plant. These curves were calculated neglecting the magnetizing current on the 66- to-12 kv transformers supplying the steel plant. When the magnetizing current for these banks is included, the curve of Figure 11 for 100 per cent power factor and the 4,000 horsepower motor will check with the 100 per cent power factor curve of Figure 10. A load at 100 per cent power factor when converted to ohms is approximately equivalent to shunting the series capacitor with that many ohms in a resistor. Since the 4,000 horsepower motor requires the greatest load to prevent self-excitation, then it dictates the ohms to be provided in parallel with the series capacitor.

Resistor Design

Figure 12 shows the resistor assembly. The top and bottom are open to give adequate ventilation. The opening, however, is covered with screens to exclude rodents, birds, and any other foreign objects.

The resistor assembly for each phase consists of four 400-ohm resistors designed for continuous operation across 7,200 volts. The connections are arranged so that one 400-ohm resistor may be operated alone or two, three, or all four may be operated in parallel. Thus, 400, 200, 133, or 100 ohms may be obtained by proper connections.

Each 400-ohm resistor consists of 72 edge-wound nickel-chrome steel-resistor units. Each unit is wound around a refractory insulator which is reinforced by a stainless steel supporting bar. The units are mounted from each end on zircon (high temperature) porcelains, between two angles, with a 7,500-volt insulator on both ends of each angle.

The resistor units for each phase are assembled in a housing 20 feet long, 7½ feet high, and 38 inches wide. The construction is such that excellent radiating characteristics are secured, thus eliminating localized heating. (The coefficient of resistance of the nickel-chrome steel resistor is 0.000295 ohm per degree centigrade.)

Conclusion

The equipment has been placed in service and staged tests have been made to determine its effectiveness and the value of resistance required. The results of these tests and a description of the substation are given in companion papers.[1,2]

References

1. 10,000 Kva Series Capacitor Improves Voltage in 66-Kv Line Supplying Large Electric Furnace Load, B. M. Jones, J. M. Arthur, C. M. Stearns, A. A. Johnson. AIEE Transactions, volume 67, 1948, pages 345–54.

2. Design and Layout of 66-Kv 10,000 Kva Series Capacitor Substation, G. B. Miller. AIEE Transactions, volume 67, 1948, pages 355–9.

3. New Series Capacitor Protective Devices, R. E. Marbury, J. B. Owens. AIEE Transactions, volume 65, 1946, pages 142–5.

4. Self-Excitation of Induction Motors with Series Capacitors, C. F. Wagner. AIEE Transactions, volume 60, 1941, pages 1241–7.

Discussion

Thomas A. E. Belt (Consulting Engineer, Lawrence, Kan.): The two companion papers[1] describing the design and the operation of the 10,000-kva series capacitor installed in the 66,000-volt Phillips-Crucible line of the Duquesne Light Company, contribute materially to the advance of electric power transmission because the data presented show that ferroresonance and subfrequencies during the starting of induction motors can be controlled and that hunting of synchronous motors can be predicted. Furthermore, over various other methods considered for correcting voltage the series capacitor proves to be more adaptable and economical.

The results obtained with this 10,000-kva installation substantiate fully the early experimental work done with the series capacitor. In this pioneering work the basic protective scheme was developed, which consisted then, as it does now, of a spark gap with a by-pass switch connected across the capacitor terminals. A resistance to prevent or to eliminate resonance also was used in this experimental work. In recent tests made on a 33,000-volt transmission system in the Middle West, to determine the feasibility of taking on heavy war loads, variable compensation even to a high degree of over compensation was introduced in the circuits. In spite of predictions and strong criticisms that the system with series capacitors would not work, the tests proved that the assertions of the critics were in error. Series capacitors were installed. Heavy war loads were carried. Better regulation of the line was obtained and the system disturbance caused by fluctuating loads was reduced. All this was accomplished at a fraction of the cost of new transmission facilities.

The following comments are based on the experience which I have had with series capacitors.

Protective Gap and By Pass Switch

Since the spark gap has a low resistance during discharge, the current will be very large and hence the duty imposed on the gap will depend upon the size of the capacitor bank. In certain tests mechanical deformation of the gap during discharge was observed, which failure might have been caused by the sudden expansion of the highly heated air between the gap surfaces. The protective gap used with the 10,000-kva capacitor has one electrode recessed within the other electrode, hence the escape of the high-temperature air during discharge tends to be restricted which may result in the building up of high pressures within the gap chamber. Have the authors any data on the pressures developed between the gap surfaces for discharges from capacitor banks of different sizes?

The time-current characteristics as shown in Figures 5 and 7, of the by-pass switch is relatively slow. For this 10,000-kva installation it is stated that the slow action is of minor importance. In cases where it is desirable to keep a system disturbance to a minimum, it would be better to use a by-pass switch having as short a time characteristic as practical. A magnetic switch without time delay and without external power requirements has been used successfully. Cannot this type of by-pass switch be developed for large installations?

Figure 6 shows that the temperature required to close the switch is 240 degrees and to open it is 200 degrees. Since the ambient temperature may vary over a wide range between summer and winter, will not the time of closing and opening be affected and if so, to what degree?

Fusing of Capacitor Terminal Connections and Blowing of Fuses

In tests made on the 33,000-volt-system mentioned previously, the heavy currents flowing during the gap discharge caused some of the terminal leads inside of the capacitor cases to fuse. Have the authors experienced such trouble? Has any precaution been taken in this 10,000-kva assembly to prevent fusing of the terminal leads? Will a prolonged gap discharge cause blowing of the protective fuses?

Series Capacitors Without Protection

The natural characteristic of capacitor dielectric is such that the short-time dielectric strength is several times its normal rated voltage at 60 cycles. Assume that a capacitor will operate without damage for one second at seven times normal potential; then, if the fault current is not more than seven times normal and does not last longer than one second, the capacitor can be installed without a protective spark gap and by-pass switch. A number of such applications are in operation. Hence, where economy is of major consideration, it may be possible to select a location in the system where the time-potential transient strength of the dielectric will not be exceeded and thus justify a series capacitor installation which otherwise could not be made.

Reference

1. See author's references 1, 2, and 3.

R. E. Marbury and **A. A. Johnson:** In reply to the discussion by Belt we believe that the series capacitor described in the paper is and will continue to be very successful in solving the problems described. We also believe this application indicates that series capacitors can be used in a number of locations where voltage correction and particularly correction of light flicker conditions is needed.

Replying to some of the questions raised in Belt's discussion we would like to say that each part of the assembly has been tested separately and as a complete operating unit.

The momentary pressures within the gap chamber were considered and numerous tests on discharge of large banks of capacitors were made. While we have no specific data on the pressures developed, nevertheless the tests indicated that the gap construction which was used is very adequate to withstand pressures from the currents which may be encountered. The inherent inductance present in the circuit involving the capacitors and the protective gap also limits the maximum peak value of current which can be obtained when a gap breaks down. On capacitor banks larger than the one discussed in the paper it is very likely that resistance will be added in the gap circuit to limit the current and to help dissipate the energy from the capacitors.

Regarding the type of switch used in this application, we recognize that this switch is not applicable where system stability is involved and there is no intention of using it in such an application. Some faster means of clearing a gap flashover is required where stability is involved. Experience has shown the need for time delay to prevent repetitive operation of the switch, particularly on distribution circuits where arcing faults may be encountered. A magnetic or thermal operated switch has serious limitations where stability is involved.

The time of closing the switch on low currents and the time of opening the switch is very much dependent on ambient temperatures. The switch operation, however, is such that it will protect the gap from damage due to prolonged arcing and this is the primary purpose of the switch in this application. The gap protects the capacitor and the switch is necessary to protect the gap.

We have experienced trouble with fuses when using metallic gaps which do not readily ionize. This point is covered in the Owens-Marbury paper given in the reference. The individual fuses used with each capacitor unit in this installation adequately protect the leads inside of the capacitor unit. Only one fuse out of 720 has been blown on this installation and this was believed to be due to a repetitive arcing of the gap caused by motor starting difficulty when the motor starting resistor was temporarily defective. A prolonged operation of a gap of the ionizing type will not damage capacitors due to the absence of oscillating currents through the capacitors.

While we recognize that the capacitor will stand considerable overvoltage for a short time, it is not reasonable or desirable to allow the voltage to reach more than approximately double its nameplate value. If the short circuit currents in a given case are such that the maximum voltage obtained across a series capacitor is within twice its rated value, then we agree that a protective device is not needed. The insulation in other apparatus is never worked nearly as high as it is in capacitors and never near the breakdown value. It is further desirable to limit the voltage during normal use, such as motor starting or short time overloads, to 150 per cent of the continuous rated voltage of the units.

Hystero-Viscosity in Silicone

MICHEL G. MALTI
MEMBER AIEE

A. K. CHATTERJEE
STUDENT AIEE

RESEARCH was undertaken to determine the variation of hystero-viscosity loss with the period of charge or discharge, with the interval between successive charges and discharges as well as with the maximum applied voltage. The material used for this purpose was silicone resin furnished by the General Electric Company and Dow Corning Company. It was found that the hystero-viscosity loss varied as follows:

$$W = aE_m^2(1-\epsilon^{-bt_i})\epsilon^{-c(t_p-T_o)^2} \qquad (1)$$

where W is the energy loss, a, b, c, T_o are positive constants of the material, E_m is the maximum voltage applied to the sample, and t_p, t_i are respectively the period of charge or discharge and the interval between successive charges or discharges.

Preparation of Sample

Silicone, in the form of a resin, was poured over a 4 inch circular disk of aluminum to a uniform thickness of 0.0257 inch and baked at 80 degrees centigrade for 2 hours, at 100° for 1.25 hours, at 120° for 2.5 hours and at 150° for 3 hours.

The solidified sample was then covered with an evaporated metal film. Thus the metal film and the aluminum disk were the two plates of a plane capacitor whose dielectric was silicone. This capacitor was placed in a sealed glass jar containing calcium chloride, to eliminate any humidity, and tested at a room temperature of about 70 degrees Fahrenheit.

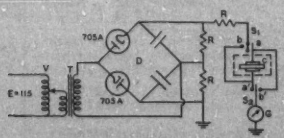

Figure 1. Circuit diagram for obtaining the hystero-viscosity loop

Test Procedure

The test circuit is shown in Figure 1 wherein E is a 60-cycle 115-volt source. V, T are variac and transformer respectively. D is a rectifier voltage doubler, using a pair of *705 A* tubes and containing a filter, which gives a steady, ripple-free, continuous voltage. R, S, C, and G are respectively resistances, switches, silicone capacitor and ballastic galvanometer. This circuit was used to obtain several hystero-viscosity loops such as those shown in Figure 2*a*. The procedure was as follows:

The output of the rectifier was adjusted for a certain voltage V_1, the switches S_1 and S_2 were closed so as to impress this voltage on the capacitor for a certain time t_p (hereafter called the period of charge or discharge). During this time the capacitor charge Q_1 was measured by the galvanometer. When t_p had elapsed, the switches S_1 and S_2 were opened for a time t_i (hereafter known as the interval between charges or discharges). At the elapse of t_i the switches S_1 and S_2 were again closed, impressing a voltage $V_2 > V_1$ on the capacitor for a period t_p. During this time the capacitor charge Q_2 was determined. At the end of the charge period the switches S_1 and S_2 were opened for an interval t_i upon the elapse of which a voltage $V_3 > V_2$ was impressed and the condenser charge Q_3 determined. This process was repeated until a maximum voltage V_m was reached and a charge Q_m obtained. After a time t_p had elapsed from the instant of impressing V_m the switches S_1 and S_2 were opened and a time t_i allowed to elapse. Then the voltage was reduced to a value $V_{m-1} < V_m$ and the switches were closed for a time t_p during which the charge retained by the capacitor was determined. The switches were again opened for a time t_i and reclosed for a time t_p at a voltage $V_{m-2} <$

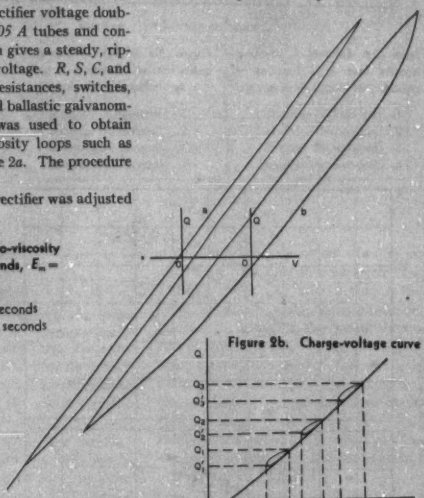

Figure 2a. Two hystero-viscosity loops for $t_p = 60$ seconds, $E_m = 1{,}500$ volts

Curve *a* for $t_i = 30$ seconds
Curve *b* for $t_i = 120$ seconds

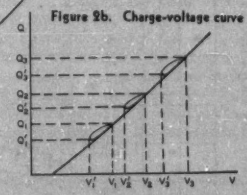

Figure 2b. Charge-voltage curve

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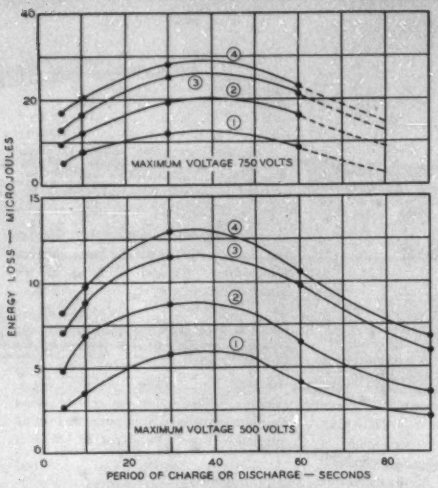
MICHEL G. MALTI is professor of electrical engineering, Cornell University, Ithaca, N. Y., and A. K. CHATTERJEE is a graduate student at the University of Illinois, Urbana, Ill.



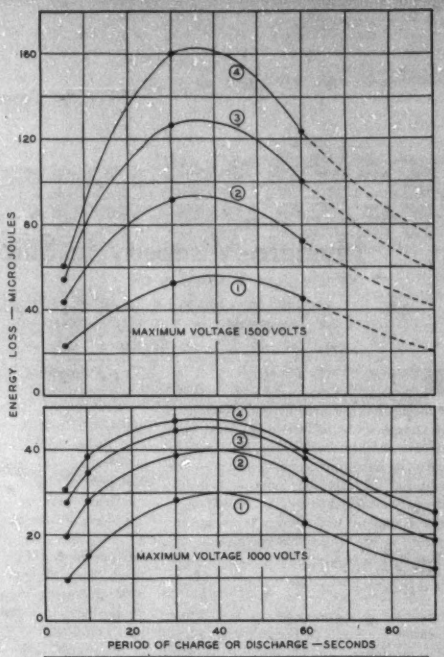
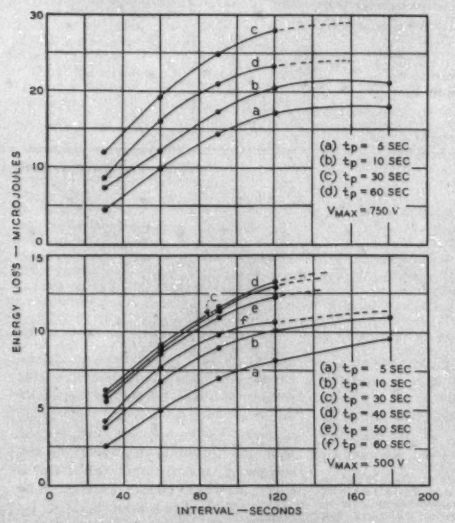


Figure 3a (above). Hystero-viscosity loss as a function of period t_p of charge or discharge

1. Interval 30 seconds
2. Interval 60 seconds
3. Interval 90 seconds
4. Interval 120 seconds

Figure 3b (right). Hystero-viscosity loss as a function of period t_p of charge or discharge

1. Interval 30 seconds
2. Interval 60 seconds
3. Interval 90 seconds
4. Interval 120 seconds



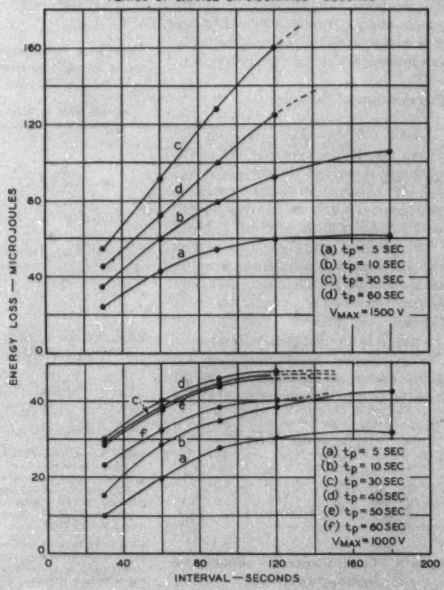
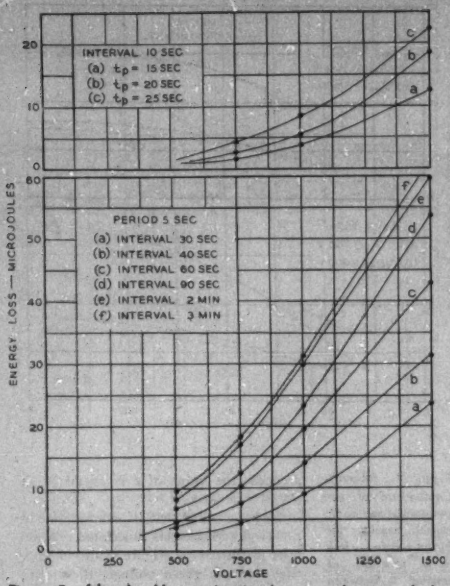


Figure 4a (above). Hystero-viscosity loss as a function of interval t_i between successive charges or discharges

Figure 4b (right). Hystero-viscosity loss as a function of interval t_i between successive charges or discharges



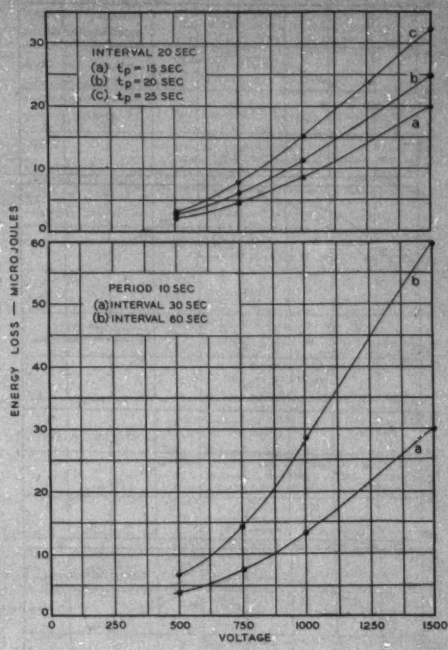


Figure 5a (above). Hystero-viscosity loss as a function of the maximum voltage E_m

Figure 5b (right). Hystero-viscosity loss as a function of the maximum voltage E_m

V_{m-1} and the charge retained by the condenser ascertained. This process was repeated until the impressed voltage became zero after which increasingly negative values of voltage were impressed on the condenser for periods t_p at intervals t_i until a voltage $-V_m$ was reached. Thereafter the absolute value of this negative voltage was reduced in varying steps for periods t_p and at intervals t_i until zero voltage was reached and the cycle was completed. The results were then plotted with acquired charge as abscissa against impressed voltage as ordinate as shown in Figure 2a. These curves are known as hystero-viscosity loops. Several loops were obtained under the following varying conditions:

1. Varying maximum voltages applied to the sample. These maximum voltages ranged from 250 to 1,500 volts.

2. Varying intervals between successive charges or discharges. The intervals ranged from 10 seconds to 3 minutes.

3. Varying periods of charge or discharge which ranged from 5 to 90 seconds.

As a result of combining these varying factors some 127 different hystero-viscosity loops were obtained.

The Hystero-Viscosity Loop

A voltage V, impressed on a capacitor, whose dielectric is other than vacuum, causes a charge $Q = CV$ only after the elapse of a certain time other than the normal period of the transient state; this extra time is consumed in some internal adjustments within the dielectric, the actual mechanism of which is not definitely known. This is why the period of charge t_p is taken into consideration in this work. Again after the impressed voltage has been removed and the capacitor allowed to stand on open circuit there is a decrease in the voltage (or charge) across its plates. This decrease is due to causes other than leakage and may be ascribed to internal adjustments within the dielectric, the actual mechanism of which is not definitely known. The longer a capacitor is allowed to remain on open circuit the greater the decrease in charge. This is why the interval t_i between successive charges is a factor in this research.

Now consider the curve, Figure 2b. When the voltage V_1 is impressed on the capacitor for a time t_p, a charge Q_1 accumulates. With the circuit open for an interval t_i, this charge is reduced to a value Q_1'. Thus, if after a time t_i the same voltage V_1 is impressed on the capacitor, the galvanometer would show a charge equal to Q_1-Q_1'. Similarly if after a time t_i a higher voltage V_2 is impressed on the capacitor, resulting in a charge Q_2 then the galvanometer would read a charge equal to Q_2-Q_1'. This charge, when added to Q_1' would give the total charge acquired by the capacitor at a voltage V_2. Thus:

$$Q_2=(Q_2-Q_1')+Q_1'=Q_2 \qquad (2)$$

In this way the charges Q_3, Q_4, Q_m corresponding to voltages V_3, V_4, V_m are also computed. The loops, Figure 2a, do not comprise the subloops shown in Figure 2b. These are omitted for two reasons:

1. We had no means of determining the potentials V_3', V_3', V_m' corresponding to Q_1', Q_2', Q_m'.

2. We had no means of determining the shapes of these subloops and hence the areas enclosed by them. It is our belief however, based on the general shape of the main loops, that the sides of these subloops are virtually

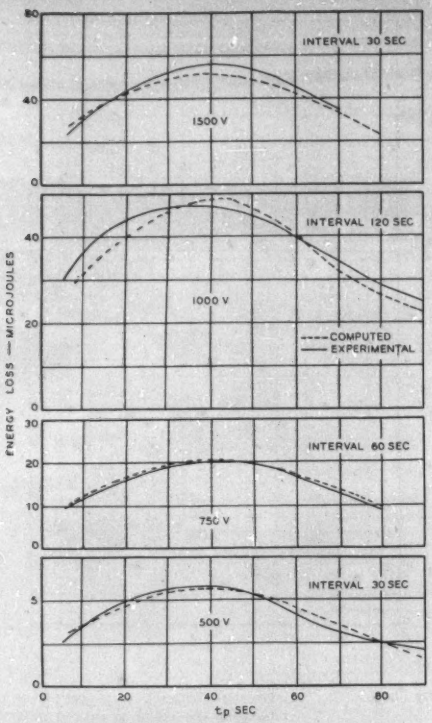


Figure 6. Comparison of experimental and computed results for hystero-viscosity loss as a function of period t_p of charge or discharge

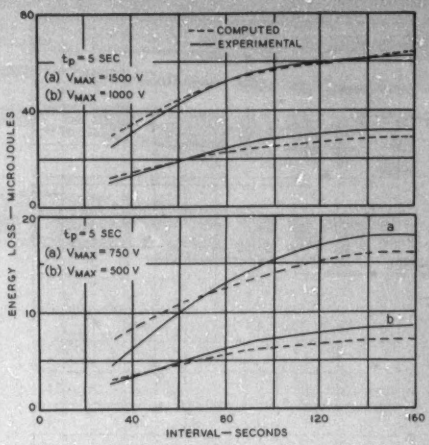


Figure 7. (above). Comparison of experimental and computed results for hystero-viscosity loss as a function of interval t_i between successive charges or discharges

straight lines. Hence the area enclosed by each subloop is extremely small as is the error of omitting the areas. This is particularly true for small intervals t_i. The results of this research are, therefore, good within this error and other errors of observation.

Hystero-Viscosity Loss Curves

It can be shown[1] that the area of the hystero-viscosity loop, Figure 2*a*, represents the energy loss per cycle per unit volume. The areas of the 127 loops were determined and, by the use of proper conversion factors, were expressed as energy loss in microjoules. Three families of curves were then plotted as follows:

1. Energy loss versus period of charge or discharge (Figures 3*a* and 3*b*).
2. Energy loss versus interval between charges or discharges (Figures 4*a* and 4*b*).
3. Energy loss versus maximum applied voltage (Figures 5*a* and 5*b*).

Expressions for Hystero-Viscosity Loss

The curves, Figures 3*a* and 3*b* resemble families of error functions whose equation is:

$$W = W_0 \epsilon^{-c(t_p - T_0)^2} \tag{3}$$

where W is the energy loss, due to hystero-viscosity, t_p is the period of charge or discharge. The constants c, T_0 and W_0 are dimensional constants whose values depend upon the units used and the dielectric tested.

A glance at the curves, Figure 3, shows that T_0 is 40 seconds. This fact leads to the conclusion that silicone suffers its greatest loss when the period of charge or discharge is 40 seconds. Now W_0, equation 3, represents the maximum loss when $t_p = T_0$. It can be obtained directly from the curves, Figure 3, and is obviously a function of the interval t_i of charge and discharge. As to the constant c, this can be determined by substituting various values of t_p and solving equation 3 for c. When the units used are microjoules and seconds, the value of c is found to be 4×10^{-4} per second.[2]

We have stated that W_0 is a function of t_i. To determine this functional relation refer to the curves, Figure 4, and observe that, for any given voltage, the curves are nearly exponential approaching a constant value as t_i increases. Hence for any maximum voltage E_m

$$W_0 = W_1(1 - \epsilon^{-bt_i}) \tag{4}$$

Two unknowns, W_1 and b, are involved in equation 4. But $W_0 \rightarrow W_1$ as $t_i \rightarrow \infty$. Hence W_1 is the asymptote of any of the curves of the voltage families, Figure 4. But by equation 3, $W = W_0$ when $t_p = T_0$. Hence W_1 is the asymptote of that curve in the voltage family, Figure 4, which corresponds to $t_p = 40$ seconds. To determine b, take various intervals t_i, read off W_0, substitute W_1 and evaluate b. When t_i is in seconds and W is in microjoules, $b = 1.8 \times 10^{-2}$ per second.

Now W_1 is a function of the maximum applied voltage E_m. The curves, Figure 5, can be fairly well satisfied by the relation

$$W_1 = aE_m^2 \tag{5}$$

where W_1 is the maximum loss corresponding to a period of 40 seconds and an infinitely large interval t_i. In order to compute the constant a we simply divide W_1 by E_m^2. If E is expressed in volts and W_1 in microjoules then $a = 5.5 \times 10^{-6}$ mho second.

Substitute equations 5 in 4 and the result in 3 and obtain:

$$W = aE_m^2(1 - \epsilon^{-bt_i})\epsilon^{-c(t_p - T_0)^2} \tag{6}$$

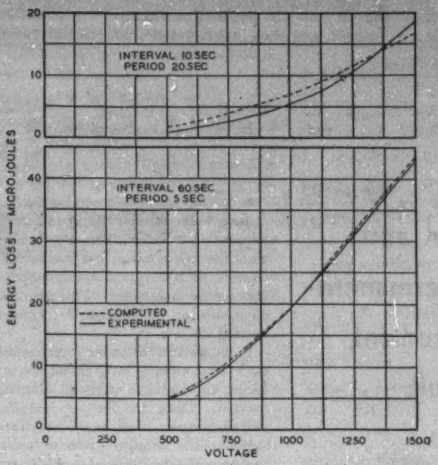


Figure 8. Comparison of experimental and computed results for hystero-viscosity loss as a function of maximum applied voltage E_m

which is the expression for the hystero-viscosity loss corresponding to any maximum voltage E_m, any interval t_i between successive charges or discharges and any period t_p of charge or discharge. It is well here to redefine the various symbols occurring in equation 6.

W = hystero-viscosity energy loss in microjoules per cycle per inch cube
a = the voltage loss-constant $a = 5.5 \times 10^{-5}$ mho second
E_m = maximum voltage in volts
b = interval loss-constant $b = 1.8 \times 10^{-12}$ per second
t_i = interval between successive charges or discharges in seconds
c = period loss-constant $c = 5 \times 10^{-4}$ per second^2
T_o = period of maximum loss $T_o = 40$ seconds

Co-ordination of Results

It is interesting to determine how the loss as computed from equations 3, 4, 5, and 6 checks with the experimental results. To do this for all the experimental curves would require more effort and engage more space than is deemed worthwhile. Computations have therefore been made for one curve of each family. The results are given in Figures 6, 7, and 8 where the broken and solid lines correspond to the computed and experimental results respectively. The agreement between computed and experimental results is remarkably close.

Considering the fact that reference 1 contains an extensive bibliography covering the period from 1865 to 1927, the authors have abstained from giving any reference prior to the year 1927.

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No Discussion

Relation Between Electrical and Mechanical Parameters of a Permanent-Magnet Movable-Coil Instrument Having a General Circuit

W. N. GOODWIN, JR.
FELLOW AIEE

Synopsis: This is an analysis of the relations between the mechanical parameters and the electrical parameters as measured at the terminals, of a permanent-magnet movable-coil instrument, or other similar electromechanical device, the movable coil of which is free to oscillate, and the circuit of which may consist of any network of resistors which in general may be unknown. It is shown further that by measuring the apparent resistance, inductance and capacitance at the terminals by a special bridge and oscillator and the current per unit deflection, that the values of the undamped period, specific damping coefficient, moment of inertia, and spring stiffness can be computed regardless of the character of the instrument circuit resistor network, or whether it be known or unknown.

IN THE THEORY of electromechanical oscillating systems, such as telephone receivers, vibration galvanometers, mechanical oscillographs, and so forth, it has long been known that the mechanical parameters, when the mechanical system is permitted to oscillate, appear to an external driving source as electrical parameters in the form of impedances.

It has been shown by Wenner[1] and by Kennelly[2] and referred to later by others,[3] that the mechanical parameters can be determined by purely electrical measurements made at the terminals, for direct connected unshunted devices.

R. W. Gilbert, in a companion paper,[4] describes a special bridge and low frequency oscillator which he has developed, by which the apparent electrical parameters of a permanent magnet movable type instrument can be measured much more simply and directly than previously possible. By analogy from available dynamic equations for instruments, he shows also how the electrical parameters as measured on his bridge can be used to compute two mechanical parameters, namely, the undamped period T_o and the specific damping coefficient n. As the latter named term is not generally known, a few words of explanation are necessary. During the period around the year 1908 while making a mathematical analysis of damped motion in instruments and galvanometers, the present author discovered that by using as one parameter, the ratio of the actual damping coefficient to the critical damping coefficient, which he designated n and gave the name specific damping coefficient, and if the time t were given in terms of the undamped period T_o, then the rather complicated equations of motion were reduced to very simple dimensionless forms[5,6] in terms of only two parameters n and T_o. This and other remarkable properties of this ratio n have been in practical use in the Weston laboratories since their discovery. Some of its properties were employed later by Drysdale and Jolley[7] and by Rich.[8]

General Statement of Problem

The object of this analysis is to provide a fundamental proof of the relations between the electrical and mechanical parameters as measured by Gilbert's bridge method, and to show that by making one additional measurement, namely, the value of the current into the binding posts required to produce a known deflection, then the other mechanical parameters, moment of inertia, and spring stiffness also can be determined. This measurement also can be accomplished without opening the case.

In the work of the earlier investigators referred to, a simple series circuit was assumed, and therefore no circuit damping resulted. Using the method here described, however, it will be shown further that all of the parameters referred to can be determined regardless of the character of the circuit in the instrument, provided that it consists of linear noninductive resistors only. That is, the circuit may consist of any kind of network of such resistors and need not be known.

If however, the circuit network is known, then the damping resistance and the fundamental magnetic parameter BAN also can be determined, where B is the magnetic flux density in the air gap, A the effective area of the coil, and N the number of turns on the coil.

Circuit Relations

Let us assume an instrument which has its movable coil connected in some manner to external terminals, and that the character of the connecting network is in general unknown, except that for this analysis it is assumed to consist of linear noninductive resistors. It is further assumed that the effect of the self-inductance of the coil at the low frequencies required in this test, may be neglected.

Now, from a well known network theorem, any resistor network, however complicated, can be reduced to a simple T section.

Let Figure 1 represent the instrument in which the unknown circuit between the binding posts and the movable coil

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W. N. Goodwin, Jr., formerly vice president in charge of research and engineering, now consulting engineer with Weston Electrical Instrument Corporation, Newark, N. J.

is reduced to the T section, a, b, and r_1 the values of which however, are also unknown, but are, nevertheless, usable in the analysis.

Analysis of General Circuit Having Electromechanical Parameters

When a current which in general is varying flows through the movable coil, a torque is developed by which a corresponding motion is produced in the movable system, as it is free to move. This motion in turn produces a counter electromotive force (emf) $BANd\theta/dt$ which for brevity let us call $Md\theta/dt$, where

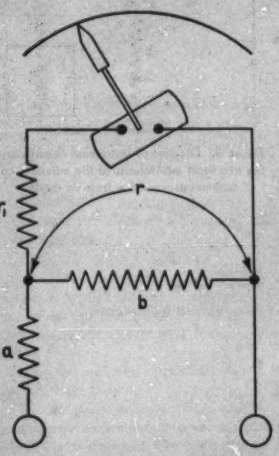

Figure 1. Instrument in which the general circuit network is reduced to an equivalent T section

$d\theta/dt$ is the angular velocity of the movable system at any time t.

Figure 2 is a simplified circuit diagram showing the resistances of the parts and the currents in them. Let e be the voltage across the terminals at any time t.

If i_1 is the current at any time through the movable coil, then the voltage across it is

$$v = ri_1 + Md\theta/dt \tag{1}$$

also for the shunt circuit

$$v = bi_2 \tag{2}$$

Then from equations 2 and 1

$$i_2 = \frac{v}{b} = \frac{ri_1 + Md\theta/dt}{b} \tag{3}$$

and the total current entering the binding posts is

$$i = i_1 + i_2 = \frac{(b+r)i_1 + Md\theta/dt}{b} \tag{4}$$

The voltage across the binding posts is then

$$e = ai + v = ai + ri_1 + Md\theta/dt \tag{5}$$

From equation 4

$$i_1 = \frac{bi - Md\theta/dt}{b+r} \tag{6}$$

Then substituting i_1 from equations 6 into 5 we have

$$e = ai + \frac{r(bi - Md\theta/dt)}{b+r} + Md\theta/dt \tag{7}$$

from which

$$\frac{d\theta}{dt} = \frac{e - i[a + rb/(b+r)]}{Mb/(b+r)} \tag{8}$$

It will be observed that the quantity $a + rb/(b+r)$ is the ordinary d-c resistance of the instrument looking in at the binding posts; let us call this r_e. Also $b/(b+r)$ is the shunting ratio, that is the ratio of the movable coil current to the total current, for unvarying currents. Let us call this q for brevity.

Equation 8 then becomes

$$\frac{d\theta}{dt} = \frac{e - r_e i}{qM} \tag{9}$$

Now, when a current i_1 passes through the movable coil, it produces a torque tending to rotate it, $BANi_1$ or Mi_1 for brevity. This torque is opposed by the inertia reaction $Kd^2\theta/dt^2$, by the damping reaction of the damping frame, air and friction, $G_f d\theta/dt$, and by the spring torque $S\theta$ at the angular position θ, where $d^2\theta/dt^2$ is the angular acceleration and $d\theta/dt$ the angular velocity. The effect of circuit damping will enter the equation later through the current produced. Therefore,

$$Mi_1 = K\frac{d^2\theta}{dt^2} + G_f\frac{d\theta}{dt} + S\theta \tag{10}$$

Then

$$i_1 = \frac{K}{M}\frac{d^2\theta}{dt^2} + \frac{G_f}{M}\frac{d\theta}{dt} + \frac{S}{M}\theta \tag{11}$$

In this equation substitute for i_1 its value in terms of i from equation 6, remembering that

$$\frac{b}{b+r} = q \text{ and } \frac{1}{b+r} = \frac{q}{b}$$

and we have

$$iq - \frac{Mq}{b}\frac{d\theta}{dt} = \frac{K}{M}\frac{d^2\theta}{dt^2} + \frac{G_f}{M}\frac{d\theta}{dt} + \frac{S}{M}\theta \tag{12}$$

Now, by differentiating equation 9 we obtain

$$\frac{d^2\theta}{dt^2} = \frac{\dfrac{de}{dt} - r_e\dfrac{di}{dt}}{qM}$$

and by integrating equation 9 we have,

$$\theta = \int\left(\frac{e - r_e i}{qM}\right)dt$$

Substituting for $d^2\theta/dt^2$, $d\theta/dt$ and θ in equation 12 the values just obtained we have,

$$iq - \frac{M}{b}\left(\frac{e - r_e i}{M}\right) = \frac{K}{M}\left(\frac{\dfrac{de}{dt} - r_e\dfrac{di}{dt}}{qM}\right) + \frac{G_f}{M}\left(\frac{e - r_e i}{qM}\right) + \frac{S}{M}\int\left(\frac{e - r_e i}{qM}\right)dt \tag{13}$$

As Gilbert's oscillator produces practically a pure sinusoidal emf, this equation can be integrated for the steady state condition very simply by remembering that in this type of circuit, if the applied emf is sinusoidal, then the resulting current is also sinusoidal having the same frequency as the driving emf but differing in phase from it. A sinusoidal function is the projection on the imaginary axis of a vector of constant absolute value revolving at a constant angular velocity ω. Therefore, letting E and I be the vector values of the emf and current respectively at the binding posts at time $t = 0$, we have

$$e = Ee^{j\omega t}, \text{ and } i = Ie^{j\omega t} \tag{14}$$

Both E and I are complex quantities and as such differ in phase, but both start from their respective phases at $t = 0$, and since they rotate at the same velocity the phase displacement remains constant. For this reason no phase angle appears in the equation 14. Then, differentiating equation 14 we have

$$\frac{de}{dt} = j\omega Ee^{j\omega t}, \text{ and } \frac{di}{dt} = j\omega Ie^{j\omega t} \tag{15}$$

and by integrating equation 14 we have

$$\int e\,dt = \int Ee^{j\omega t}dt = \frac{Ee^{j\omega t}}{j\omega}$$

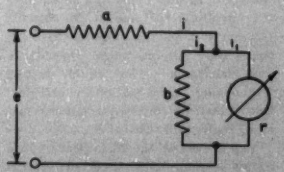

Figure 2. Diagram of equivalent instrument circuit

and

$$\int i dt = \int I e^{j\omega t} dt = \frac{I e^{j\omega t}}{j\omega} \tag{16}$$

Substituting the values given in equations 15 and 16 in equation 13 and dividing through by $e^{j\omega t}$ which appears as a factor in each quantity, we have,

$$Iq - \frac{E}{b} + \frac{r_o I}{b} = \frac{K}{M^2} \frac{j\omega E}{q} - \frac{K r_o j\omega^2}{M^2 q} + \frac{G_f}{M^2}\left(\frac{E - r_o I}{q}\right) + \frac{SE}{j\omega M^2 q} - \frac{S r_o I}{j\omega M^2 q} \tag{17}$$

Collecting terms in E and I, and combining the reactive terms, we find that the apparent impedance of the instrument looking in from the binding posts is

$$Z = \frac{E}{I} = \frac{q^2 + \frac{r_o q}{b} + \frac{G_f r_o}{M^2} + j\left(\frac{K r_o \omega}{M^2} - \frac{S r_o}{M^2 \omega}\right)}{\frac{q}{b} + \frac{G_f}{M^2} + j\left(\frac{K\omega}{M^2} - \frac{S}{M^2\omega}\right)} \tag{18}$$

In Gilbert's bridge method, which will be referred to later in detail, the resistance of the instrument, measurable on the bridge by direct current, is automatically subtracted from the total impedance. The impedance back of r_o, which is the motional impedance, is then mathematically expressed as $Z_m = Z - r_o$.

Then subtracting r_o from Z in equation 18 and simplifying we have

$$Z_m = Z - r_o = \frac{1}{\frac{1}{q^2 R_d} + j\left(\frac{K\omega}{q^2 M^2} - \frac{S}{q^2 M^2 \omega}\right)} \tag{19}$$

where $R_d = 1/(q/b + G_f/M^2)$ is the combined equivalent damping resistance resulting from circuit, damping frame, and so forth.

This equation gives the apparent electrical impedance produced by the vibratory motion of the movable system as a result of the applied a-c voltage, in terms of the mechanical parameters.

Analysis of Equivalent Electric Circuit

In equation 11 it will be noted that the current is the sum of three components which can be duplicated in a purely electric circuit by the sum of the currents through three parallel circuits, consisting respectively of resistance, capacitance and inductance. One could surmise from analogy which mechanical and electrical constants correspond, but we shall await analysis for the answer.

Figure 3 shows the equivalent electric circuit in which C, L and R are the capacitance, inductance, and resistance respectively, in parallel as indicated by the mechanical equation 11. The resistors a, b and r are the same as given in Figure 2 for the electromechanical problem.

We shall determine the impedance of the circuit looking in at the terminals, for a sinusoidal voltage E applied at the terminals, and then subtract the d-c resistance r_o as was done in the electromechanical problem.

By the usual laws of electric circuits, the impedance looking in at the binding posts can be written directly as

$$Z = a + \frac{\left[\frac{1}{\frac{1}{R} + j\omega C + \frac{1}{j\omega L}} + r\right] b}{b + r + \frac{1}{\frac{1}{R} + j\omega C + \frac{1}{j\omega L}}}$$

Combining the reactive elements, and simplifying, we have

$$Z = \frac{(b+a) + [br + a(b+r)]\left[\frac{1}{R} + j\left(C\omega - \frac{1}{L\omega}\right)\right]}{1 + (b+r)\left[\frac{1}{R} + j\left(C\omega - \frac{1}{L\omega}\right)\right]} \tag{20}$$

But $br + a(b+r) = r_o(b+r)$, since the resistance of the circuit measured at the binding posts with direct current is $r_o = a + br/(b+r)$

Then equation 20 becomes

$$Z = \frac{(b+a) + r_o(b+r)\left[\frac{1}{R} + j\left(C\omega - \frac{1}{L\omega}\right)\right]}{1 + (b+r)\left[\frac{1}{R} + j\left(C\omega - \frac{1}{L\omega}\right)\right]} \tag{21}$$

As was done in the electromechanical problem, subtract from the total impedance Z, the d-c resistance r_o measured at the binding posts and we have after simplifying, the motional impedance

$$Z_m = Z - r_o = \frac{\frac{b^2}{(b+r)^2}}{\frac{1}{b+r} + \frac{1}{R} + j\left(C\omega - \frac{1}{L\omega}\right)} \tag{22}$$

As in the electromechanical problem, let the shunting factor $b/(b+r)$ be designated q for brevity, then

$$Z_m = \frac{q^2}{\frac{q}{b} + \frac{1}{R} + j\left(C\omega - \frac{1}{L\omega}\right)} = \frac{1}{\frac{1}{q^2 R_d} + j\left(\frac{C\omega}{q^2} - \frac{1}{Lq^2\omega}\right)} \tag{23}$$

where

$$R_d = 1/(q/b + 1/R)$$

is the combined equivalent damping resistance resulting from circuit, damping frame, and so forth.

Comparison of Electric and Electromechanical Circuits

Comparing equation 23 for the purely electric circuit with equation 19 for the electromechanical circuit, it is obvious that to have the same impedance under all conditions in both cases the following fundamental relations must exist.

$$\frac{q}{b} + \frac{G_f}{M^2} = \frac{q}{b} + \frac{1}{R}$$

$$\frac{K\omega}{q^2 M^2} = \frac{C\omega}{q^2}$$

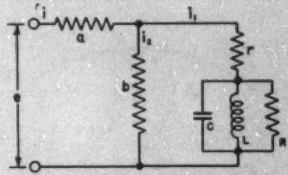

Figure 3. Diagram of instrument circuit showing electrical equivalents of the movable coil parameters, resulting from its motion

and

$$\frac{S}{q^2 M^2 \omega} = \frac{1}{Lq^2\omega}$$

From which it follows that,

$$\frac{G_f}{M^2} = \frac{1}{R} \tag{24}$$

$$\frac{K}{M^2} = C \tag{25}$$

$$\frac{S}{M^2} = \frac{1}{L} \tag{26}$$

These equations give the fundamental relations of the mechanical parameters to their electrical equivalents for frame, friction and air damping only for an unshunted instrument. As the shunting ratio, q, in general is not known, R, L, and C, and the corresponding mechanical parameters cannot be measured directly nor computed. However, from equation 23, the apparent damping conductance is

$$\frac{\left(\frac{q}{b} + \frac{1}{R}\right)}{q_2} = \frac{1}{q^2 R_d}$$

which designate for brevity as $1/R_d$. R_d is the motional resistance resulting from all damping including circuit damping, and R_d the corresponding apparent motional resistance. The apparent capacitance is C/q^2 and the apparent inductance Lq^2, which designate for brevity as C_a and L_a respectively. These values can be meas-

ured by Gilbert's method using his bridge and oscillator, but as applied to the general circuit problem.

Then the corresponding instrument parameters from equations 19 and 23 are, respectively,

$$\frac{1}{R_a}=\frac{1}{q^2}\left(\frac{g}{b}+\frac{G_f}{M^2}\right)=\frac{1}{q^2R_d} \tag{27}$$

$$C_a=\frac{K}{M^2q^2} \tag{28}$$

and

$$L_a=\frac{M^2q^2}{S} \tag{29}$$

In equation 23 substitute the values C_a, L_a, and R_a adopted for brevity and we have

$$Z_m=\frac{1}{\frac{1}{R_a}+j\left(C_a\omega-\frac{1}{L_a\omega}\right)} \tag{30}$$

This is the electrical equivalent of the electromechanical problem, the relations of which are given in equations 27, 28, and 29.

Gilbert's Bridge as Applied to the General Problem of a Shunted Instrument

It will now be shown how Gilbert's method is applied to the measurement of the equivalent electrical quantities in equation 30 for the general instrument circuit.

Set up the bridge as shown in Figure 4. Let the bridge ratio be P.

The instrument is connected in arm A_1 in which the upper circuit is the d-c circuit of the instrument, the resistance of which is $r_o=a+br/(b+r)$ and the lower circuit corresponds to the motional conductance resulting from the motion of the movable system produced by the alternating current, in which $R_a=q^2R_d$ is the apparent motional resistance

$$\frac{q^2}{\frac{g}{b}+\frac{Gf}{M^2}}$$

and C_a and L_a the apparent capacitance and inductance, respectively, of the shunted instrument.

Bridge Measurement Procedure

The same method in general will be used as that originally used by Gilbert, which as applied to the general case of the shunted instrument is as follows:

1. With PR_1 short circuited, the resistance Pr_o in bridge arm A_2 is adjusted by using a d-c source until the bridge is balanced. Then the resistance r_o in arm A_2 is equal to the d-c resistance of the instrument circuit

$$r_o=a+\frac{rb}{b+r}$$

in A_1. This resistor Pr_o, is not changed during the test. This performs electrically the mathematical operation of subtracting r_o in equation 22.

2. With the capacitor C_1 disconnected and alternating current used as the source, the frequency and the resistor PR_1 are both adjusted for a bridge balance, and as a result, the measured frequency and movable system are in resonance. Let this frequency be f_o. Then from equation 30 for the angular frequency $\omega_o=2\pi f_o$,

$$C_a\omega_o=\frac{1}{L_a\omega_o} \tag{31}$$

From this the undamped period of the instrument can be computed as

$$T_o=\frac{1}{f_o}=2\pi\sqrt{L_aC_a} \tag{32}$$

Furthermore, from the principle of the bridge, and the fact that at resonance the reactive components cancel, the apparent motional resistance

$$R_a=q^2R_d=R_1 \tag{33}$$

3. With the capacitor C_1 connected, the frequency is increased to a value f_1 or an angular frequency of $\omega_1=2\pi f_1$ at which the reactance of the capacitor,

$$\frac{1}{2\pi f_1C_1}$$

is equal to the resistance R_1, that is

$$\frac{1}{2\pi f_1C_1}=\frac{T_1}{2\pi C_1}=R_1 \tag{34}$$

or

$$T_1=2\pi R_1C \tag{35}$$

where T_1 is the period corresponding to the frequency f_1.

This adjustment Gilbert accomplishes by a running slide rule computation.

It follows then from the principle of the bridge that since the impedance of the capacitor C_1 is equal to the resistance R_1 as a result of the adjustment given in equation 34, then the combined conductance of the L_a and C_a circuits in the instrument is equal to the apparent motional conductance $1/R_a$.

That is, at the frequency f_1,

$$\frac{1}{R_a}=C_a\omega_1-\frac{1}{L_a\omega_1}=2\pi f_1C_a-\frac{1}{2\pi f_1L_a} \tag{36}$$

Computation of Instrument Parameters from Bridge Measurements

By solving equations 31 and 36 as simultaneous equations, and substituting for ω_0 and ω_1 their values in terms of T_0 and T_1, respectively, we obtain the apparent inductance

$$L_a=R_a\left(\frac{T_0^2-T_1^2}{2\pi T_1}\right) \tag{37}$$

and the apparent capacitance

$$C_a=\frac{T_1T_0^2}{2\pi R_a(T_0^2-T_1^2)} \tag{37a}$$

Having thus computed the apparent inductance L_a, the apparent capacitance C_a, and remembering that the apparent damping resistance R_a was measured by the bridge to be R_1, we can compute the specific damping coefficient as follows:

From the theory of damping it is known that the critical damping coefficient is

$$G_o=2\sqrt{KS} \tag{38}$$

From equations 28 and 29, the moment of inertia

$$K=M^2q^2C_a$$

and the spring stiffness

$$S=\frac{M^2q^2}{L_a}$$

Substitute these values in equation 38 and we have

$$G_o=2M^2q^2\sqrt{\frac{C_a}{L_a}}$$

Now, from its definition, as given in the list of symbols, the actual damping coefficient, corresponding to the damping resistance R_d, is

$$G=\frac{M^2}{R_d}=\frac{M^2q^2}{R_a}$$

since the damping coefficient of a damped system is

$$\frac{(BAN)^2}{R_d}$$

where R_d is the effective damping resistance. Then since by definition, the specific damping coefficient n is the ratio of the actual to the critical damping coefficient, we have

$$n=\frac{G}{G_o}=\frac{\frac{M^2q^2}{R_a}}{2M^2q^2\sqrt{\frac{C_a}{L_a}}}=\frac{\sqrt{\frac{L_a}{C_a}}}{2R_a} \tag{39}$$

from which n can be computed from the measured values L_a, C_a, and R_a.

The specific damping coefficient n may have any value from zero to infinity. When n is less than unity, the instrument is underdamped, when $n=1$ critical damping results, and when n exceeds unity the instrument is overdamped. Un-

der the latter condition, n is very difficult to measure directly but is readily determined by the bridge method.

The undamped period from equation 32 is

$$T_0 = 2\pi\sqrt{L_a C_a}$$

It is thus proved that the undamped period T_o and the specific damping coefficient n can be determined in an instrument with a general circuit by the purely bridge method, and that they can be determined by this method whether the instrument is shunted or not, and that the character of the shunting circuit, however complex, need not be known, so long as it consists of linear noninductive resistors only. The damping resistance R_d also can be determined if q is known, by equation 27.

Determination of Other Instrument Parameters Not Measurable by the Bridge Method Alone

As stated previously, only two independent parameters, T_o and n can be measured in general by the bridge method alone. However, by one additional electrical measurement, which also can be accomplished without opening the case, namely, the measurement of the current into the binding posts required to produce a known deflection, then, both the moment of inertia K, and the spring strength S, can be determined regardless of the character of the shunting resistance network.

The magnetic parameter $M = BAN$, and the damping resistance R_d, can be computed if the shunting ratio is known.

Let this measured current be I_o abamperes per radian deflection. Then the current through the movable-coil per radian is

$$i_m = \frac{bI_o}{b+r} = qI_o \tag{40}$$

Now, since the electromagnetic torque produced by L_m is balanced by the spring torque, then

$$BANi_m = S$$

or

$$BANqI_o = MqI_o = S \tag{41}$$

where S is the spring torque per unit angle in dyne centimeter per radian. From equation 41

$$Mq = \frac{S}{I_o} \tag{42}$$

then from equations 28 and 29

$$K = G_a M^2 q^2$$

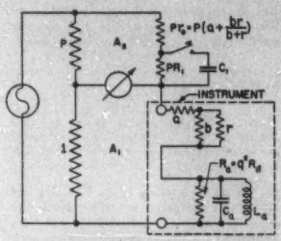

Figure 4. Basic diagram of bridge circuit showing the electrical equivalents of the motional parameters of the instrument in series with its d-c resistance

and

$$S = \frac{M^2q^2}{L_a}$$

Then substitute the values for Mq given in equation 42 and solving the two equations we obtain

$$K = I_o^2 L_a^2 C_a \tag{43}$$

and

$$S = I_o^2 L_a \tag{44}$$

by which the moment of inertia, and spring strength can be computed from the measured values L_a, C_a by the bridge, and the measurement of the current into the binding posts per unit deflection angle, and the results are independent of the shunting ratio.

Determination of magnetic constant $M = BAN$.

From equations 41 and 44

$$Mq = \frac{S}{I_o} = \frac{I^2 L_a}{I_o} = I_o L_a \tag{45}$$

This shows that the magnetic parameter M can not be determined unless the shunting ratio

$$q = \frac{b}{b+r}$$

is known. If the instrument is unshunted, then $q = 1$ and

$$M = IL_a \tag{46}$$

Example

To show the application of the foregoing principles, the mechanical and electromechanical parameters of a permanent magnet movable coil microammeter were determined by direct mechanical and electrical measurement in the usual manner. The apparent electrical parameters were then measured on Gilbert's bridge by an operator to whom the character of the circuit was entirely unknown. The following apparent electrical values as measured on the bridge were reported.

The apparent motional resistance, inductance and capacitance are as follows respectively:

R_a = (103.0 ohms) = 103.0×10^9 abohms
L_a = (80.0 henrys) = 80.0×10^9 abhenrys
C_a = (2,290 microfarads) = $2{,}290 \times 10^{-15}$ abfarads

The current I_o per radian deflection as measured was 6.15×10^{-6} abampere.

From these data the electromechanical parameters were computed by the equations given, as follows:

The specific damping coefficient from equation 39, is

$$n = \frac{\sqrt{\frac{L_a}{C_a}}}{2R_d} = \frac{\sqrt{\frac{80\times10^9}{2290\times10^{-15}}}}{2\times103\times10^9} = 0.907$$

The undamped period, from equation 32, is

$$T_o = 2\pi\sqrt{L_a C_a} = 2\pi\sqrt{80\times2290\times10^{-6}} = 2.69 \text{ seconds}$$

The moment of inertia from equation 43, is

$$K = I_o^2 L_a^2 C_a^2 = (6.15)^2\times10^{-12}\times80^2\times10^{18}\times2290\times10^{-15}$$

or

$$K = 0.554 \text{ gram centimeter}^2$$

The spring torque per unit angle, from equation 44, is

$$S = I_o^2 L_a = (6.15)^2\times10^{-12}\times80\times10^9 = 3.05 \text{ dyne centimeter per radian}$$

In the general circuit, where the character of the circuit is unknown, the electromagnetic parameter $M = BAN$, and the motional resistance R_d cannot be separated from the unknown shunting factor q. The values $Mq = I_o L_a$, and $R_d q^2 = R_a$ may be obtained from equations 45 and 33, respectively.

In the example given here, however, the circuit was known to the present writer but not to the bridge operator. The movable coil circuit resistance was $r = 64.1$ ohms, and this was shunted by a resistor $b = 170$ ohms, therefore $q = b/(b+r) = 0.725$.

From this value the parameters $M = BAN$ and R_d may be computed as follows:

The electromagnetic parameter from equation 45, is

$$M = BAN = \frac{I_o L_a}{q} = \frac{6.15\times10^{-6}\times80\times10^9}{0.725} = 6.78\times10^5$$

or

$M = 6.78\times10^5$ dyne centimeter per abampere

The motional resistance, from equation 33, is

$$R_d = \frac{R_a}{q^2} = \frac{103}{0.526} = 196 \text{ ohms}$$

The following is a list of the values of the same parameters obtained by the usual direct measurement, and for comparison, the results of the bridge method shown below.

	Direct Measurement	By Bridge
Specific damping coefficient n, numeric	0.885	0.907
Undamped period T_n, seconds	2.70	2.69
Moment of inertia K, grams centimeter^2	0.548	0.544
Spring stiffness, dyne centimeter per radian	2.96	3.05
Electromagnetic parameter, $M = BAN$, dyne centimeter per abampere	6.6×10^8	6.78×10^8
Motional resistance $R_d/10^8$, ohms	209	196

List of Symbols Used

A = effective area of the coil, centimeter^2
a = series resistance in instrument network
B = magnetic flux density, gauses
b = shunt resistance in instrument network
C = equivalent capacitance, abfarads
C_a = apparent capacitance as measured on bridge, abfarads
E = emf vector across binding posts at $t = 0$
e = applied vector electromotive force at any time t
ϵ = basis of natural logarithms
G_f = damping coefficient produced by the damping frame, if any, and the air and friction = damping torque per unit angular velocity, dyne centimeter per radian per second
I = a-c current vector into instrument circuit at binding posts at $t = 0$
I_n = current to deflect the movable system, per unit angle, abamperes per radian
i = current vector into binding posts at any time t
$j = \sqrt{-1}$
K = moment of inertia, grams centimeter^2
L = equivalent inductance, abhenrys
L_a = apparent inductance as measured on bridge, abhenrys
$M = BAN$, for brevity
N = number of turns on the movable-coil
n = specific damping coefficient = ratio of actual damping coefficient to that for critical damping
q = shunting ratio $= b/(b+r)$
r = resistance of movable-coil circuit
r_a = measured d-c resistance of instrument
R = equivalent resistance resulting from the motion of the system only; abohms
R_a = total apparent damping resistance, as measured on bridge, abohms
S = spring torque per unit angle; dyne centimeter per radian
T_n = undamped period, seconds
t = time, seconds
θ = angular deviation of the movable system from its position of equilibrium at any time, produced by the sinusoidal forces
ω = angular frequency, radians per second

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No Discussion

A Telegraph Signal Analyzer

G. L. ERICKSON
MEMBER AIEE

THE maintenance of accurate and reliable service over modern automatic telegraph circuits has necessitated the development of testing equipment capable of measuring transmission quality and detecting the causes for transmission irregularities. The telegraph signal analyzer described herein is a small, portable test set designed for this purpose and intended for field use on start-stop telegraph systems. It can be employed for straightaway testing with any form of transmitted text, and hence permits observations of distortion on working circuits carrying miscellaneous signals. The signal analyzer has also proved very useful in measuring the transmission capabilities of telegraph circuits and investigating the relative importance of the various components that make up the total distortion. Other applications include measurements to determine the condition of signals delivered by transmitting keyboards or regenerative repeaters and tests to check the operation of receiving printers. Measurements are made conveniently, rapidly, and with a degree of accuracy which is ample for all field purposes.

When compared with other test equipment similar in purpose, the outstanding feature of this telegraph signal analyzer is that it has been made very simple and very compact. The complete set is much smaller and lighter than a printer, and is also very much cheaper. As shown in the photograph of Figure 1, it is contained in a small metal box measuring only 6 by 9 by 8 inches and weighing only 18 pounds. Two cords are provided for making connections to a source of 110-volt a-c power and to the circuit being tested. On the face of the panel there is a jack and a power outlet which permit a printer to be inserted in the same circuit. The panel also carries the analyzer unit, a power switch, and a second switch for starting and stopping the test. During the test period an observation is made of the distortion on every individual signal pulse as it is received. This has the distinct advantage of indicating the effect of occasional peaks, as well as the average distortion, and also makes it easy to identify troubles that occur systematically. The test results are presented in the form of a permanent record on a paper chart which may be analyzed in detail, measured quantitatively, and retained for future reference.

The printers most commonly used on start-stop telegraph circuits are the 65 word per minute teleprinter employing a 7-unit code, and the 60-speed teletypewriter with a 7.4-unit code. The signal analyzer can be used with either of these codes. It will be recalled that the basis of transmission in these systems is a set of seven pulses which is transmitted for each character. A marking signal is used for the stop pulse transmitted during the idle condition. The first pulse of each character consists of a spacing signal, the start pulse, which is followed by five intelligence pulses. Each of the intelligence pulses may be either spacing or marking. The seventh pulse is the marking stop pulse again, which is transmitted until the start of the next character. From the signal diagram shown in Figure 2, it is evident that the only difference between the 7-unit code and the 7.4-unit code is found in the duration of the stop pulse. In the 7-unit code equal-length pulses are used, and each is of 22 milliseconds (.022 seconds) duration. The pulse lengths are exactly the same for the 7.4-unit system except that the stop pulse is somewhat longer and lasts for 31 milliseconds (.031 seconds). Of course the distinction between the two codes exists only if signals are transmitted at top speed by some form of automatic machine. When a keyboard is operated manually the stop pulses are seldom as short as this and they may be very much longer.

Signals are considered as perfect whenever they are timed as shown in the diagram (Figure 2) to the extent that each of the first six pulses is exactly 22 milli-

Figure 1. Signal analyzer in operation

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G. L. ERICKSON is in the engineering department, Western Union Telegraph Company, New York, N. Y.

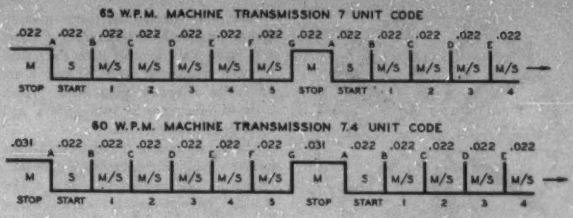


Figure 2. Telegraph signal codes

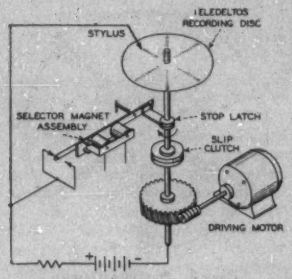


Figure 3. Simplified sketch of mechanism

seconds long. The seven points marked *A*, *B*, *C*, *D*, *E*, *F*, and *G* represent the instants at which signal transitions may be made from marking to spacing or vice versa for distortionless operation. The effect of signal distortion is to advance or retard one or more of the transition points so that it no longer occurs in its proper position with respect to transition *A*, the beginning of the start pulse.

To measure the distortion it is necessary to determine the amount that any transition has been shifted, remembering that time intervals must be measured from instant of the start transition. In the signal analyzer this has been done by establishing reference points 22 milliseconds apart beginning with the arrival of the start pulse, and by then recording with respect to these reference points all transitions as they occur. A circular paper chart is rotated through one complete revolution whenever a printer character is received, and each signal transition in the code is caused to make a mark on the paper. The departure of these marks from radial lines printed on the chart at 22-millisecond intervals is a measure of the distortion.

The operating principle of the analyzer mechanism will be explained with the aid of Figure 3. A start-stop shaft is driven through a slip clutch by a 60-cycle synchronous motor geared down to 420 rpm. The chart is a four-inch disk of Teledeltos recording paper fastened to a small hub at the end of this shaft. When the selector magnet is energized by a marking stop pulse, the stop latch is engaged to prevent the shaft from turning. As soon as a spacing start pulse arrives, the magnet releases the latch and the shaft is free to rotate while the code pulses for one character are being received. The stop pulse at the end of the character brings the shaft to rest again after the chart has made one revolution.

Meanwhile a stylus in contact with the surface of the disk has been energized by the signals and has recorded a series of black marks on the paper. Because it is desired to record only the transitions between pulses, the recording voltage does not appear on the stylus as long as the tongue of the selector magnet is on either its spacing or its marking contact. But while the tongue is moving through the gap between contacts, the stylus receives a short impulse which lasts as long as the tongue is not touching either contact. As a result, current flows from the stylus to the Teledeltos paper during each signal transition and causes a small black mark to be recorded on its electrically-sensitive surface. The recording voltage, represented as a battery in the sketch, is supplied by a rectifier contained within the set.

The recording stylus is moved radially across the paper from the center of the disk to its outer edge by means of a mechanism not shown in the illustration. When it reaches the outer edge of the chart, the stylus returns to the center again and then continue to move slowly back and forth in this manner as long as the test is continued. In each trip across the chart, the signal transitions for approximately 190 characters are recorded. Signals may be recorded on one chart for a period of a half hour or longer.

When the signals received by the analyzer are of perfect quality, free from bias or other distortion, the alignment of the marks recorded on the chart will appear as shown in Figure 4. The seven radial lines printed on the chart at *A*, *B*, *C*, *D*, *E*, *F*, and *G* are reference marks 22 milliseconds apart, corresponding to the signal transition points similarly labeled in Figure 2. Undistorted signals cause all of the marks made by the stylus to fall on these lines. With distortion present the transitions will occur earlier or later than their normal times, and will appear on the chart displaced to an extent deter-

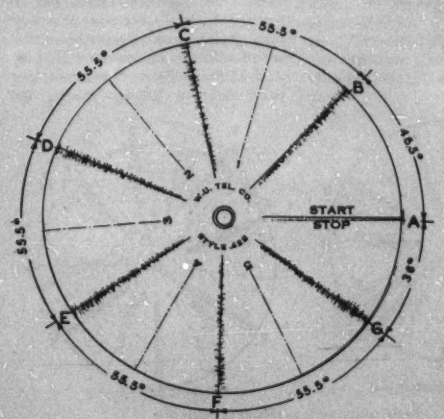


Figure 4 (left). Analyzer chart with perfect signals

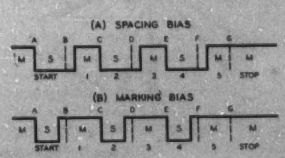
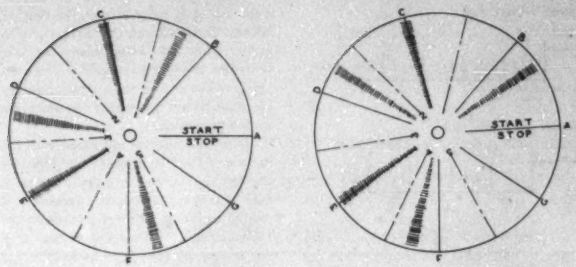


Figure 5 (right). Biased signals, letter "y"



A—25 per cent spacing bias B—25 per cent marking bias

Figure 6. Bias charts, letter "Y"

mined by the amount of distortion. The dotted lines 1 to 5 represent the centers of the intelligence pulses and are helpful in estimating the amount of distortion loss.

The speed of the start-stop shaft in the analyzer is 420 rpm which means that the chart is rotated through an angle of 55.5 degrees in 22 milliseconds when running freely without clutch slip. In Figure 4 it will be noted that this is the angle subtended by each of the five intelligence pulses on the chart. When the shaft has been stopped and then released again, a small amount of slip occurs in the clutch before the chart is accelerated to its free speed. The inertia of the rotating parts has been minimized by supporting the paper disk only at the center, but nevertheless the angle for the start pulse is necessarily reduced slightly. The clutch is so adjusted that the chart rotates 46.5 degrees during the start pulse. With a start pulse and five intelligence pulses totaling 324 degrees, an angle of 36 degrees remains for the stop pulse. This is sufficient to insure that the shaft is stopped at the end of each complete revolution even though the stop pulse occurs as much as 14.3 milliseconds late, corresponding to a signal distortion of 65 per cent.

In discussing the effects of signal distortion, it will be of interest to consider the case of bias. Bias is a systematic distortion that displaces every signal transition with respect to its position in undistorted signals. All of the mark-to-space transitions are uniformly shifted in one direction and all of the space-to-mark transitions in the opposite direction. With spacing bias, the directions are such as to lengthen the spacing pulses and shorten the marking pulses. With marking bias, the marking pulses are lengthened at the expense of the spacing. In either case, the net effect of bias in a start-stop system shows up only on the space-to-mark transitions and never on the mark-to-space transitions. The reason for this is made evident by Figure 5, which shows the signal code combination for the letter "Y" with either (*A*) spacing or (*B*) marking bias. It will be noted that transitions *C* and *E* are always properly timed with respect to transition *A*, the beginning of the start pulse. This is because *A*, *C*, and *E* are all mark-to-space transitions and consequently they have all been displaced by the bias exactly the same amount. Hence the effective displacement of *C* and *E* is zero. On the other hand the space-to-mark transitions at *B*, *D*, and *F* are uniformly retarded from their proper times of occurrence by spacing bias, and advanced by marking bias.

The appearance of the analyzer charts for the signals just described is shown in Figure 6. The marks recorded at *C* and *E* fall on the reference lines in both charts. Those at *B*, *D*, and *F* are late in (*A*) and early in (*B*). Since the marks fall mid-

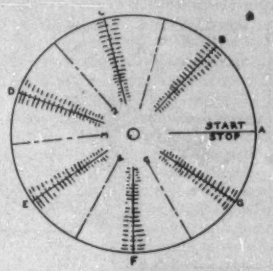
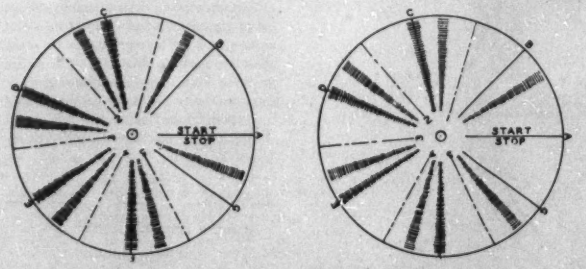


Figure 8. Fortuitous distortion

way between the reference lines and the pulse center lines, they indicate a bias loss of about 25 per cent. Note that there is no transition recorded at *G* because the fifth intelligence pulse is marking, the same as the stop pulse, for this particular signal.

Figure 7 shows charts obtained with the same amount of bias when transmitting miscellaneous signals. At each of the reference points, transitions occur in some of the code combinations and are absent from others. Whenever transitions do occur at *B* and *G*, they must always be from spacing to marking and hence they are all shifted by the amount of the bias. At *C*, *D*, *E*, and *F* transitions can occur in either direction between spacing and



A—25 per cent spacing bias B—25 per cent marking bias

Figure 7. Bias charts, miscellaneous signals

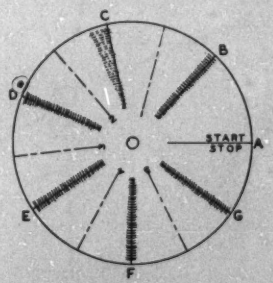
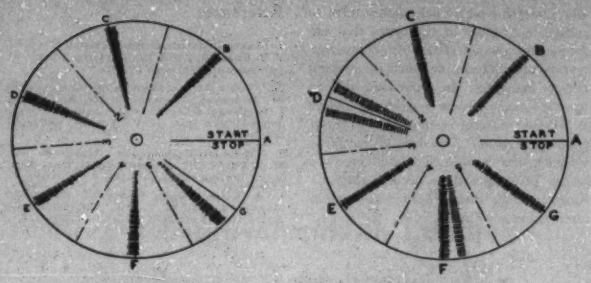


Figure 9. Defective



A—Defective stop contact B—Contact overlap

Figure 10. Tests of cam transmitters

marking, and therefore some appear correctly timed while others are displaced by the bias.

Fortuitous distortion causes the signal transitions to be shifted about in a random fashion so that some transitions are advanced and others are retarded in varying degrees. This causes the marks recorded on the chart to be dispersed in the manner shown by Figure 8. Usually the distortion experienced on a telegraph circuit is due to a combination of bias and fortuitous effects. Then the chart resembles one of those in Figure 7, but includes also the erratic characteristics of Figure 8. The two components are easily separated because the dispersal of the marks at *C*, *D*, *E*, and *F* is caused by both effects, while that at *B* and *G* is due to fortuitous distortion alone. When characteristic distortion is also present, it can be detected by comparing the results of measurements using different recurring test signals.

Examples of charts obtained when using the signal analyzer to check the output of transmitting equipment are given in Figures 9, 10, and 11. In general when there is some irregularity in a sending mechanism, whether it be a printer keyboard, tape transmitter or regenerative repeater, the defect will be manifest as a systematic displacement of certain transitions. The analyzer provides a convenient means for determining the cause of the trouble and following the effect of steps taken to correct it.

In a commutator-type transmitter the signal pulses are sent from a segmented distributor by a rotating brush arm. If one segment becomes worn or dirty, the sending brush will fail to make a positive contact and the signal will be mutilated every time a marking pulse is applied to that segment. Figure 9 shows an example in which the erratic behavior of transition *C* indicates that the segment for pulse number 2 is defective.

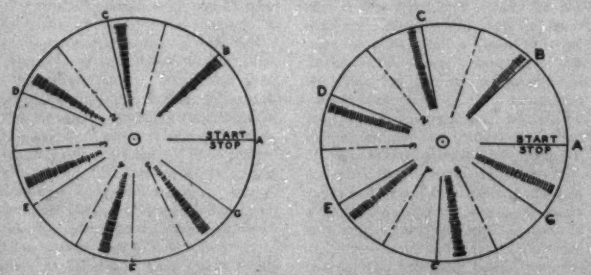
In a cam-type transmitter the pulses are sent from a set of contacts which are operated in sequence by rotating cams. The signal transitions will be improperly timed if a cam is defective or if a contact spring is out of adjustment. One example of this is illustrated in Figure 10 (*A*) where the displacement of transition *G* shows that the start-stop contact always closes earlier than normal. If the start-stop contact opens incorrectly, it will advance or retard the start transition and cause all subsequent transitions to be out of place by a fixed amount.

When irregularities are present in the cams or contacts that transmit the intelligence pulses, the marks recorded on the chart by miscellaneous signals will usually overlap in the manner shown by Figure 10 (*B*). The double line of marks at *D* indicates that transitions produced by the opening of contact number 2 are shifted in one direction while transitions due to the closing of contact number 3 are shifted in the opposite direction. The marks at *F* consist of one group perfectly aligned and a second group occurring later than normal. Obviously contact number 4 is opening too late or contact number 5 is closing too late. The exact nature of the trouble is readily determined by observing recurring signals, such as the letter "Y" as compared with the letter "R."

If the speed of a transmitter is too fast or too slow, the displacement of transitions will be as shown in Figure 11 (*A*) and (*B*) respectively. When the speed is too fast the transitions occur early and the marks are advanced by an amount which becomes progressively greater around the chart. When the speed is too slow the marks are retarded similarly.

The operation of a receiving printer can be checked with the signal analyzer by comparing the signal quality revealed by a test chart with the printing range actually obtainable. A defective printing mechanism is indicated whenever the printer fails to yield the expected range. A printer in good condition gives a range of about 80 points with perfect signals. The loss of range due to signal distortion is estimated by observing the transition displacement angle on the chart and comparing it with the 55.5-degree angle between reference lines, which represents one pulse or 100 points of range. It has been found possible to predict the loss within five points by visual inspection alone. Greater accuracy can be secured if a protractor or specially graduated scale is used to measure the displacement angle.

Of course, the analyzer mechanism itself must be properly adjusted if a high degree of accuracy in the distortion measurements is required. However, the device is exceptionally rugged and dependable and requires little maintenance attention once the necessary adjustments have been correctly made. Since the analyzer will always reveal its own imperfections, it is only necessary to check its performance from time to time by driving it with a source of perfect signals.



A—Sender too fast B—Sender too slow

Figure 11. Tests of transmitter speed

Only a few of the more common circuit and equipment troubles have been illustrated by the chart records presented in the foregoing discussion. The telegraph signal analyzer can be relied upon to disclose any type of signal impairment experienced on start-stop telegraph circuits. In some cases careful study and analysis may be required to diagnose obscure troubles and determine the cause of transmission irregularities revealed by a test chart. Nevertheless, it has been found that operating personnel quickly learn to recognize troubles of the types that are frequently encountered. By comparing test results with sample charts illustrating typical examples, they are soon able to use the signal analyzer to good advantage. When the analyzer is employed to check circuit operation periodically, incipient troubles can be detected and corrected before printing failure occurs, thus assuring a high standard of transmission quality and continuity.

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4. A Cathode-Ray Telegraph Distortion Measuring Set, **W. T. Rea**, *Bell Laboratories Record*, (New York, N. Y.) April 1947, page 150.

Discussion

J. D. McLellan (J. H. Bunnell and Company, Brooklyn, N. Y.): Erickson's paper on the Telegraph Signal Analyzer has been read with great interest by myself and other members of J. H. Bunnell and Company. From a practical application standpoint we feel that the telegraph signal analyzer is a great step forward in automatic telegraph servicing.

When your printer goes out of service or does not work correctly, it is necessary to call in a skilled mechanic, who has in the past been much handicapped by not being able to take with him the proper testing equipment to quickly analyze the trouble and prescribe for it. He is in the same position as a mechanic trying to adjust timing, and set-up the ignition and carburation of one of the modern cars without the modern testing equipment to assist him. The telegraph signal analyzer puts into a small package sufficient testing equipment to analyze and definitely locate the trouble with the printer and with other high-speed automatic telegraph equipment.

We feel that the use of the telegraph signal analyzer brings the adjustments necessary to correct minor deficiencies within the reach of the amateur, making it unnecessary to call in an expert printer man for a minor adjustment.

Our first experience with the telegraph signal analyzer was at a Convention of the Association of American Railways in Miami where we were demonstrating automatic telegraph equipment. The distributor transmitter was evidently thrown out of adjustment during the shipping process to Miami. Skilled mechanics were not available at the demonstration, but a telegraph signal analyzer was, which definitely pointed out which segment on the commutator was not in proper adjustment. The correction took only a matter of minutes and the whole display was quickly put under way.

In fact our own test department is very anxious to put them in use, as soon as we can fill the customers' demand for them.

In short, we feel that the telegraph signal analyzer will fill a long felt requirement, that is, the test equipment can be taken to the seat of the trouble obviating the necessity of taking the trouble to the test equipment.

Recent Improvements in Loading Apparatus for Telephone Cables

S. G. HALE
MEMBER AIEE

A. L. QUINLAN
MEMBER AIEE

J. E. RANGES
NONMEMBER AIEE

Synopsis: Through the use of improved materials, manufacturing techniques, and designs, a series of exchange-area loading coils has been provided which is equivalent electrically to the superseded types but requires one-third less copper and has considerably smaller over-all dimensions. Similarly, 3-coil toll cable loading units have been provided with a saving of one-half in both copper and core material, with a small sacrifice in electrical behavior as compared with superseded types. The reduced size of the new coils and units, together with improved assembly arrangements, made possible a 65 per cent saving in the volume and weight of the cases housing them.

MOLYBDENUM permalloy powder has been available for several years and has been employed in large quantities in magnetic cored coils for loading voice-frequency cable circuits in the telephone plant. This core material has been described, together with its various applications, by Legg and Given.[1]

Improvements in materials and techniques that have become available recently have made possible substantial advances in the design of loading coils. A new insulating enamel for copper wire, known as "Formex,"[2] was found to be much superior to earlier wire enamels in its resistance to abrasion and cracking when flexed sharply. Because of these characteristics it was found to be satisfactory for winding toroidal coils without the supplementary textile insulation formerly required over the enamel. This permitted a considerable increase in the utilization of available winding space. As a result of developments in coil winding facilities it became commercially possible to wind toroidal coils to inner diameters as small as 0.35-inch. This marks a distinct improvement over previous technique, where an inner coil diameter of 0.5 inch was required to accommodate the winding machine shuttle.

These improvements were such as to be worthwhile, if coil designs were revised to take advantage of them. They could be utilized to provide coils having either improved characteristics or reduced size and cost, compared with previous types. A program was undertaken to develop new loading coils and suitable cases to house them so as to achieve the greatest size reduction and saving of materials, consistent with maintaining the existing performance standards. This program was carried to completion for the class of coils used in loading paired conductors of exchange area cable. Preliminary work was also carried out along these lines for three-coil units of the type employed for loading quadded toll cable.

Before commercial manufacture of the new toll coils could be started, however, the demands of World War II made it imperative to reduce the consumption of strategic materials. The most critical items employed in loading coil manufacture were nickel, required for the permalloy core material, and copper for the windings. A study was made to determine the most effective way of reducing the nickel and copper requirements with the minimum loss in performance of loaded cable circuits. No substantial economy of materials could be realized however by any reasonable modification of the new exchange area coil types. A one-third reduction in copper requirements had already been effected and even a drastic reduction of core volume would save only a relatively small amount of nickel since each core contains only one ounce. The toll coil cores, however, employed approximately five ounces of nickel each, consequently they offered the greatest opportunity for the saving of nickel. Data indicated that the transmission penalty in toll cable circuits resulting from a 50 per cent reduction of core volume would be appreciable but could be tolerated. Therefore, the new toll coils were designed on this basis, providing a considerable reduction in the total Bell System consumption of nickel.

The design program outlined previously did not involve any change in the permalloy powder core material, but did require consideration of core and coil configurations to find the optimum dimensional relations. Nonlinear distortion, hysteresis loss and susceptibility to superposed d-c magnetization are directly related to the magnetic flux density in the core. It can be shown that the flux density per unit current varies inversely as the square root of the core volume (within 1.5 per cent for core sizes under consideration), for a given core material and value of coil inductance. The size and shape of a toroidal coil may therefore be changed without appreciably affecting the above characteristics if the core volume remains constant. Reduction of the mean core diameter however, permits more efficient use of a given quantity of core material and fewer winding turns are required. Modifications in core area can be made to secure the desired volume.

Through the use of less bulky wire insulation and improved winding facilities it was possible to reduce materially the size of the exchange area coils and yet maintain the electrical characteristics of the previous types. A 50 per cent reduction in volume of the toll coil with only a slight impairment in transmission performance was thus achieved. The reduced coil sizes, together with improved assembly methods in the redesigned cases, made possible substantial reductions in case sizes and weights.

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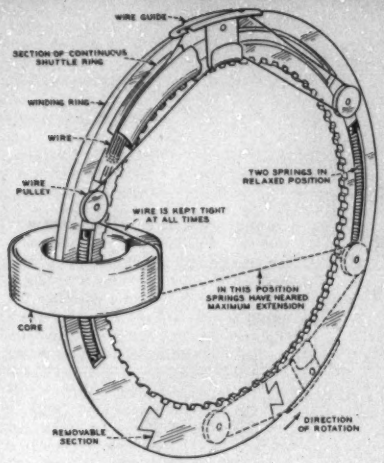
S. G. HALE and J. E. RANGES are both with the Bell Telephone Laboratories, Inc., New York, N. Y. and A. L. QUINLAN is with the Western Electric Company, Chicago, Ill.



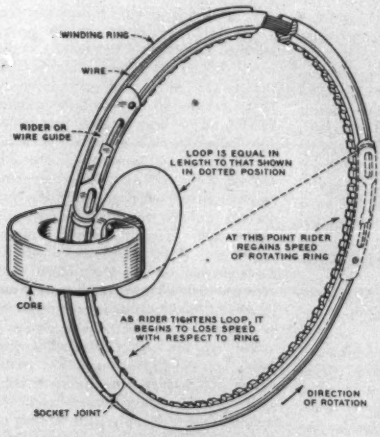


Figure 1 (above, left). Two-ring winding machine

Figure 3 (above, right). Present single-ring winding machine with ring sprung open to receive core

Figure 2 (left). Single-ring winding machine

Winding Machine Improvements

Winding machine improvements included in the present project are considerable, from the standpoint of minimum hole size of the winding, which defines the size of shuttle used in applying the wire to the winding. These improvements have been described primarily in patents.[3] Recent papers[4,5] analyze the behavior of the two-ring machine, which has been the standard form of winding machine for many years. The present improvements can be best understood by reference to the winding operation of the 2-ring machine, which is illustrated in Figure 1. To wind a coil, the dove-tailed and latched sections of both winding rings are removed, while placing the magnetic core ring in position for winding. Wire is then applied to the shuttle ring by driving it forward to take wire from the supply spool. The outer end of the wire is then carried through the guide eye mounted on the guide ring, and around the tensioning pulleys and fastened to the core. Both rings are then driven forward through a differential gear to carry the wire around and down through the hole in the core for the desired number of turns.

One of the major problems in the two-ring type of machine is to prevent excessive tension on the wire as it is applied to the core. The design of the earlier 2-ring machines did not incorporate means for relieving the wire stresses which resulted from the pull necessary to cyclicly accelerate the wire-filled shuttle relative to the driven guide ring. Thus, in order to avoid wire breakage, the machines had to be operated at low winding speeds, with considerable trouble being encountered from the loop of excess wire which was formed during a part of each revolution after the wire guide eye had passed the point of maximum distance from the core.

Figure 1 shows how the recent 2-ring machines use springs and sliding pulleys to take up the loop of wire as the wire-guide approaches that part of its path where it enters the hole in the core, and to give wire out again as it approaches the opposite side of its path. This improvement allowed higher operating speeds, since only the pulleys, their supporting sliders and portions of the springs needed cyclic acceleration. Since these elements had been reduced in mass to the maximum extent consistent with serviceability, the possibility of further speed increases or the adoption of smaller wire sizes depended upon the development of new design features involving less mass.

The tempting savings to be obtained from smaller coils, using smaller wire sizes, could not be realized until a better solution to the problem was found. Also,

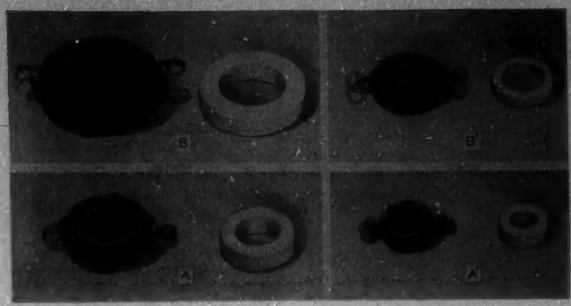


Figure 4. Comparison of the new (A, A') and superseded (B, B') cores and coils: A and B, toll cable loading coils; A' and B', exchange area loading coils

Figure 5. Partial and complete assemblies of new (lower) and superseded (upper) toll cable loading units

Figure 6. New (right) and superseded (left) exchange area loading coils suitably encased for individual installation in cable splicing chambers

it became obvious that if a greater proportion of the area occupied by the winding rings could be devoted to wire holding capacity, a coil with a given amount of wire volume could be wound with a smaller hole size. When these conditions were added to the space savings obtainable by the use of the new synthetic wire insulations, with their ability to stand more severe bending and abrasion conditions, as compared with previous enamels or combinations with cotton or silk, new methods and radical changes in the winding ring designs were warranted.

The latest single-ring winding machine, Figure 2, with sliding wire-guide was developed to overcome the outlined limitations. The springs and pulleys in the 2-ring designs have been replaced by a light frictionally engaged sliding wire guide which allows the wire to form a loose loop that is controlled by guide or tensioning plates positioned along each side of the wire path. Now the wire must leave the shuttle around one side of the ring instead of on the center line between the rings as is the case with the 2-ring designs. Nevertheless, the turns of the winding are applied in a substantially radial direction on the core by guide plates appropriately located. A somewhat greater tendency exists with this machine for successive turns to cross each other instead of lying side by side in a layer, but the point of crossover is usually on the outer periphery so "turns space" in the hole of the coil is only slightly wasted.

The ultimate practical limit in size reduction of coils is problematical. As coil size is reduced, the saving in cost of materials assumes a diminishing proportion of total cost. The problem becomes one of maintaining economic balance between the value of space savings to the customer, and the manufacturing cost.

The new ring, Figure 2, developed to wind the smaller exchange area loading coils, being similar to the slightly larger one for the toll coils, is here described. This ring has a mean diameter of three inches and a cross-section diameter of 0.25 inch, and will wind coils having a 0.35-inch hole diameter. It is essentially a thin walled, hollow ring, designed to give maximum wire capacity in the hollow center, with adequate rigidity of structure. The outer surface of one side wall serves as the wire-guide-slider track.

The wire-guide which is caused to slide on this circular track by the pull of the wire as needed, is a short piece cut from a formed ring made to the same diameter as the winding ring. This process insures a suitable fit on the sliding surfaces of the ring, and maximum efficiency in the use of the 0.25-inch cross-section diameter. One end of the wire-guide carries a groove through which the wire slides as it approaches the coil core. The other end provides adjustable sliding spring tension on the slider track of the ring.

A portion of the inner periphery of the winding ring is utilized to provide supporting shoulders and driving gear teeth. With the assistance of modern high strength alloy steels and heat treating methods, suitable wall thickness and diameter dimensions were determined to provide sufficient rigidity of the ring structure to permit operation at speeds up to 1,800 revolutions per minute and still to be flexible enough to be sprung open at a single joint for the loading of the core and the unloading of the coil.

The small size of the new winding ring made it impracticable to construct the conventional dove-tailed and latched removable section. However, as noted before, the ring is made flexible so that, with a single joint, it can be twisted enough out of line to enter or leave the coil. The ring joint is designed to be self-keying. It must be capable of opening and closing many times without causing sufficient wear to change appreciably the pitch spacing between the gear teeth adjacent to the joint, or destroying the alignment of the slider track on the side of the ring, since this could reduce the uniformity of slider friction, causing winding irregularities.

Summarizing, the new single-ring machine consumes less than 50 per cent as much winding space as was the case with the former 2-ring machines. The latest model, Figure 3 winds at a speed of 1,500 turns per minute for wire sizes from 26 to 38 gauge and, when operated at 750 turns per minute, it will wind wire several sizes smaller. The ring is driven by a

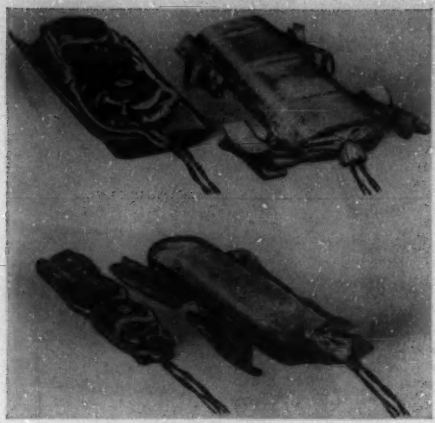


Figure 7 (left). Partial and complete assemblies of new (lower) and superseded (upper) toll cable loading units arranged for individual installation in cable splicing chambers

Figure 8 (right). New (right) and superseded (left) individual cases, containing exchange area loading coils, suitable for burial directly in the ground with rubber covered paired wire

geared and belted 2-speed motor with electric brake which is arranged to stop the machine automatically for both loading the shuttle and winding the coil when preset turn counts are reached.

Exchange Area Coils

Since exchange area cable circuits are relatively short, small gauge paired conductors are employed, and small coils having relatively high resistances are satisfactory. As noted herein, in redesigning this class of loading coil the objective was to obtain the smallest possible coil that could be produced commercially, consistent with maintaining the grade of performance of the previous coils. This involved such factors as nonlinear distortion and energy losses in the core, winding resistance, minimum inside winding diameter, ability of winding machines to handle various wire gauges and storage capacity of the winding-machine shuttles. Consideration of these and other factors resulted in the core and coil designs illustrated in Figure 4 in comparison with the previous types. As may be seen, the core diameters are reduced and the axial height is increased. Although this maintains core volume identical with the superseded coil, it entails a coil volume only 60 per cent as large, and the more efficient configuration requires fewer winding turns per unit inductance. Due to the smaller number of turns and the space saving provided by the use of "Formex" wire, the wire size was reduced to effect a 1/3 saving in copper, with no increase in resistance over that of the previous coils. Pertinent dimensional data on the old and new types are given in Table I. The coil winding comprises two equal sections, applied on the two halves of the core, to provide the required circuit balance. This coil type includes several codes having different inductances, and together with suitable cases, described in Table I, has been in commercial production since the latter part of 1941.

Toll Cable Coils and Units

The coils employed for loading voice-frequency toll cable circuits are subject to more stringent requirements than are the exchange area types previously discussed. Those requirements pertaining to d-c resistance, core losses, and nonlinear distortion necessitate the use of a larger core, which in the previous designs had five times the volume of that employed in the small exchange area coils. To conserve nickel, the cores for the new toll cable coils were reduced in volume by 50 per cent.

By properly correlating the factors of reduced core volume, use of "Formex" wire of smaller gauges, and winding machines capable of winding such wire to as small as 0.5-inch inside coil diameter instead of the former 0.75-inch minimum, the new coil designs shown in Figure 4 were worked out. The previous toll cable coil and core are also shown for comparison purposes. The improved configuration of the new core is quite apparent. Dimensional data for both old and new types are given in Table I. The new coil requires 50 per cent less winding copper than did the old one yet, due to the use of the improved features, the winding resistance is only 25 per cent greater than that of the old coil. The hysteresis loss in the core is increased approximately 40 per cent by the reduction in volume, but the increase of total loss is proportionally smaller since other loss components are unaffected by the change in volume.

As described by Shaw and Fondiller[6] voice frequency toll cable circuits are generally quadded, and a phantom circuit is derived from the two side circuits of a quad. A practical arrangement for loading these three circuits consists of a 3-coil unit having two side circuit coils and one phantom circuit coil. The coils are wound with interleaved windings to provide a

Table I

	Small Exchange Area Coils		Three-Coil Toll Cable Units	
	Superseded	New	Superseded	New
Core Data				
Outside diameter, in	1.35	1.06	2.25	1.57
Inside diameter, in	0.92	0.56	1.40	0.95
Axial height, in	0.35	0.44	0.55	0.57
Volume, cu in	0.26	0.26	1.30	0.65
Weight, lb	0.075	0.075	0.38	0.19
Coil Data				
Outside diameter, in	1.65	1.22	2.56	1.88
Axial height, in	0.65	0.65	1.00	0.88
Effective cylindrical volume, cu in	1.4	0.75	5.1	2.2
Weight, lb	0.14	0.12	0.68	0.34
Unit Data				
Diameter, in			3.5	*2.25–2.65
Height, in			5.0	3.88
Volume, cu in			48.2	21.4
Weight, lb			2.6	1.3

* U-shaped cross-section instead of cylindrical as in case of superseded unit.

high degree of balance and magnetic coupling. Each line winding of a side circuit coil comprises an inner section on one-half of the core and an outer section on the other half. The phantom coil windings are similarly divided, each of the four line windings comprising an inner section on one quadrant of the core and an outer section on the diametrically opposite quadrant. The two line windings in each side circuit are therefore closely associated as the inner and outer winding sections on opposite quadrants of the core. When the windings are properly interconnected the consequent close coupling and low leakage prevents the introduction of any appreciable inductance into the side circuits by the phantom coil.

For loading each toll cable quad, two side circuit coils and one phantom coil are assembled together as a unit, both electrically and mechanically. Figure 5 illustrates a partial and a complete unit assembly of both the old and new designs. The three coils are stacked on a central wooden dowel between suitably insulated clamping plates. In the old type units the wooden dowel was drilled longitudinally to accommodate a through bolt employed for mounting the units in the cases. A fibre plate attached to the upper clamping plate carried terminals for connections between the coils and to the case wiring. On this terminal plate were also mounted small toroidal coils and bug-

Figure 10. New (right) and superseded (left) cases containing 51 exchange area loading coils

type resistances employed for crosstalk balancing purposes. In order to obtain acceptably low values of crosstalk between the phantom and side circuits of loading units it is essential that the series and shunt impedances of each side circuit winding be closely balanced. It is generally necessary to add small increments of resistance or inductance or both, in series with the deficient winding to achieve the required degree of balance. Iron washers insulated by fibre washers are inserted between coils to reduce crosstalk between them. Residual side-to-side crosstalk coupling in the phantom coil is counteracted by an opposing coupling between the adjacent side circuit coils, adjusted by rotation of the coils with respect to each other on the central dowel.

Both old and new type units were provided with sheet metal containers for electrical shielding and mechanical protection. In the old type this was cylindrical in form while in the new type it has a U-shaped cross-section with the terminal plate mounted on the open side. Due to the small size of the coils in the new unit, the central dowel serves only to locate the coils, and it is pinned between the metal end plates. Tabs extending from one end of the U-shaped casing serve to anchor the unit in place when assembled in the loading unit case. As may be seen in Figure 5, the small crosstalk balancing coils for the new unit are mounted on one end-plate which is depressed sufficiently for this purpose, while the bug-type balancing resistances lie on the terminal plate. The entire unit, of both old and new types is vacuum impregnated with compound to retard moisture absorption during handling and storage, and to increase dielectric strength.

It should be noted that in spite of the 25 per cent increase in loading unit resistances employing the small coils previously described, the effect on the attenuation, at 1,000 cycles per second, of 19-gauge toll cable circuits is an increase ranging only from 1 to 3 per cent, depending upon the weight of loading. This amounts to approximately 0.25 decibel in

Figure 9. New (lower, partial and complete) and superseded (upper) 5-coil assemblies of exchange area loading coils

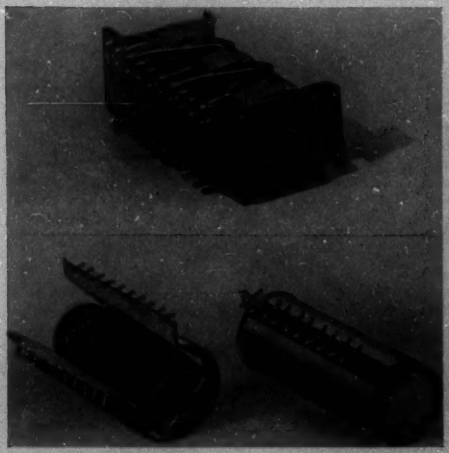


Figure 11. Internal assembly and principal parts of new type case for housing 51 exchange area loading coils

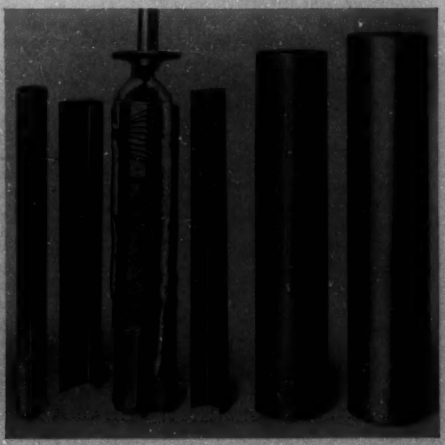
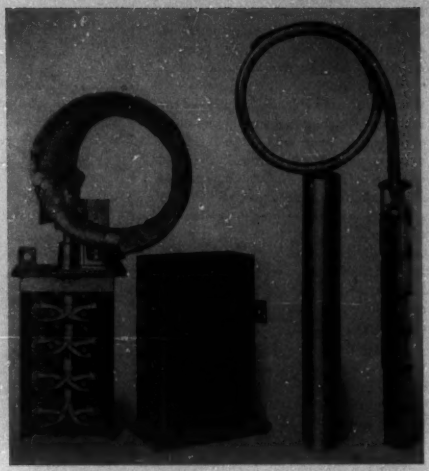


Figure 12 (left). New (right) and superseded (left) cases for use with aerial cables, containing 200 exchange area loading coils

Figure 13 (below, left). New type case containing 100 exchange area loading coils, provided with suitable finish for use in underground vaults; shown mounted on a platform for shipment

Figure 15 (right). Internal assemblies of new (right) and superseded (left) cases for housing 6 and 8 toll cable loading units, respectively

Figure 14. Internal assembly of new type case for housing 100 exchange area coils



a 50-mile repeater section for the loading system now most frequently installed.

Splice Loading Facilities

In certain instances where it is desired to load only a few circuits in new or existing cables or to replace damaged coils, it may be most expedient to incorporate the coils or units in a cable splicing sleeve. The reduced size of the new coils and units provides a valuable gain in this application, since appreciably smaller and lighter lead splice sleeves can be used than was possible with the former coils.

The exchange area coils have been provided with cases which, as illustrated in Figure 6, are essentially the same for both old and new types except for the difference in size. The case consists of a heavy paper box, with connecting leads extending from one side, and is provided with cotton tapes for tying in place in the splice. The entire assembly is thoroughly dried and wax impregnated to exclude moisture during periods of storage and handling. The encased coils as shown may be installed individually or as groups of six coils mounted on a fibre strip.

Toll cable loading units, electrically the same as those previously described but modified mechanically for incorporation in splicing sleeves are shown in Figure 7. The three coils are mounted on a fibre strip, and a sheet of heavy paper is folded around the unit. The entire assembly is placed in a muslin bag and wax impregnated. The space saving of the new unit over its predecessor is apparent.

A related application for the exchange-area type coil is that of loading long subscriber loops and farmer lines, where a rubber covered pair is buried directly in the ground. Figure 8 shows the new and superseded cases for housing coils arranged so that they may be buried when used with the rubber covered wire. The old case comprised a rather large metal container treated with an asphaltic compound to prevent corrosion. The case housing the new small coil comprises a metal ring around the coil which serves as mechanical protection and support for terminal lead connections, moulded into a soft rubber jacket to exclude moisture.

Loading Coil Cases, General

The greatly reduced size and weight of the new loading coils and units led naturally to the consideration of lighter weight materials in the design of smaller cases to house the coils. A study of various types of case structure resulted in the adoption of a cylindrical arrangement for all cases, the diameters and lengths being varied as required. The superseded cases for both exchange area and toll cable loading were rectangular in cross section except for a few of the smallest sizes which were cylindrical. The new coils and units are mounted on lightweight steel panels which are stacked one above another and assembled in the cylindrical case shells. This makes for economy of different parts and flexibility in handling a range of potting complements, since the number of panel assemblies may be varied as re-

quired, using the same diameter of cylindrical tubing cut to appropriate lengths. Consequently only a few different case diameters are required. Except in certain of the smallest size cases, the mounting panels are circular and the conductors extending from the lead covered stub cable (by means of which connection is made to the main cable) pass through central holes in the panels and branch off at appropriate points for connection to the coils mounted on the panels.

Improvements in stub cable construction were also incorporated in the new cases. These consisted of a 100 per cent increase in dielectric strength between core and cable sheath, for greater protection against lightning damage; reduction of lead sheath thickness to decrease weight and increase flexibility; reduction of crosstalk by new types and increased separation of quads; elimination of brass tape shielding between directional groups, with substitution of paper wrapping; and increased range in the lengths of stub cable that can be furnished as a part of a case, to minimize the need for extra splicing in the field. Cases for smaller complements of coils employ cylindrical lead sleeves with light steel internal reinforcing walls, with steel or brass end plates soldered in place, through one of which passes the stub cable. The lead sleeve cases are suitable for aerial use or in underground cable vaults without modification.

Cylindrical steel casings with steel base plates welded in place are employed for the larger size cases. Through the use of thinner material and the reduced size, the weights of the new cases are approximately 65 per cent less than those of the superseded cases housing the same numbers of coils. The steel cases are used, with slight modifications of mounting details and protective finish, for both aerial and underground installation. Those intended for aerial use have a protective paint finish to prevent corrosion, while those for installation in underground vaults are provided with a composite finish consisting of layers of cloth and asphalt. When cases are to be buried directly in the soil, particularly in areas infested with rodents known to damage lead, the case finish also includes iron wire mesh over exposed lead parts, and the stub cable is protected by the addition of jute wrappings and steel armor. Aerial cases are provided with simple brackets for mounting on poles, eliminating the elaborate pole balconies formerly required for all but the smallest cases. Cases for use in cable vaults are provided with slotted base plates on one end and brackets at the other, for installation on the floor, or in any position on the wall.

After completion, all types of cases are vacuum-dried for several hours by alternate applications of dried air and vacuum through the stub cable. No filling compound is used in these cases, in contrast with the practice generally employed in the superseded types. Instead they are filled with dried air at a pressure of approximately nine pounds per square inch before shipment. When received by the customer the pressure is checked to verify the absence of leaks.

It has been possible to simplify greatly the packing arrangements for the new cases. Formerly it was necessary to employ heavy crates mounted on skids, to insure the cases being handled in an upright position. Also, special steel guards, which were returned by the customer after installation, were required to support the stub cables. The new cases and their stub cables, however, are simply secured to wooden platforms in such a way as to avoid damage to the stub cable. This assembly is then placed in a lightweight packing box which may be handled in any position.

Exchange Area Coil Cases

For purposes of assembly in cases, the previous exchange area coils were assembled on wooden dowels in groups of five or thirteen coils, held in place by end plates which also served as mounting brackets. Such a mounting arrangement was impractical with the reduced dimensions of the new coils. Instead they are assembled in groups of five coils in a sheet-metal container as shown in Figure 9 which illustrates the assembly of both the old and new 5-coil units. This constitutes the basic coil unit employed in all the new cases for this type of coil, with the exception of those for the splice loading applications described previously. The coils are maintained in their positions by the walls of the container and an insulating liner. Two edges of this insulator project through a longitudinal slit in the side of the container and provide a mounting for terminals to which the coil windings are connected by short extensions of

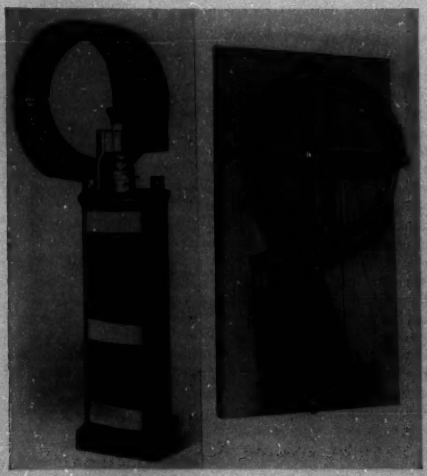
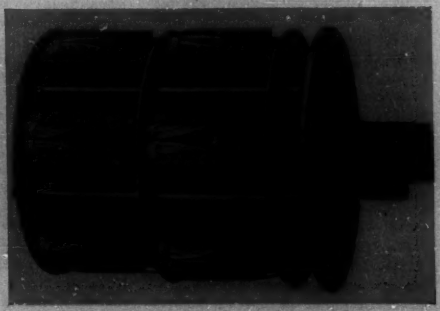


Figure 16 (left). New (right) and superseded (left) cases containing 12 and 14 toll cable loading units, respectively

Figure 17. Internal construction of new case for housing 12 toll cable loading units



the winding conductors. A dished metal cover is stapled in place and applies sufficient pressure to prevent coil movement. A central eyelet type of projection drawn from the bottom of the container, plus a small off-center nub serve as locating media for mounting purposes. This assembly is 50 per cent lighter and has 60 per cent less volume than the superseded five-coil assembly.

Lead sleeve cases are employed for complements ranging from 5 to 51 coils. Although former lead sleeve cases contained up to 100 coils and this number could have been increased with the new coils, steel cases proved to be more economical for complements exceeding 51 coils. A typical lead sleeve case of the new design, housing from 35 to 51 coils is shown in Figure 10, together with its predecessor. Figure 11 shows the internal construction of the case and its principle parts. Three of the coil units are mounted on each of the four triangular mounting plates. Although 60 coils can be accommodated, the usual maximum is 51, since this type of case is normally associated with 51-pair cables.

Cases having tubular steel walls with steel-bottom plates welded in place are provided to house coils in complements ranging from 75 to 450 coils. As pointed out, the steel cases for aerial and underground applications differ only in the matters of mounting details and protective finishes. Figure 12 shows old and new type aerial cases housing 200 coils. Figure 13 shows a similar type 100-coil case having suitable finish and mounting details for underground use, mounted on a wooden platform for shipment. The coil mounting panel employed in cases housing up to 200 coils is circular and accommodates five coil-units which are evenly spaced around the outer periphery of the panel. An internal case assembly of the 100 coil size is shown in Figure 14. Cases containing 300 to 450 coils are assembled with panels mounting ten coil-units per panel, arranged in two concentric circles with five coil-units each. The units in the outer circle are staggered with respect to those in the inner circle to provide accessibility to the terminals of the inner group. This arrangement makes for good utilization of space and at the same time provides ample room for the stub cable conductors which are brought down through the central holes in the panels and branched out between the coil units to the appropriate terminals.

Since the new steel cases have the cable run through the center of the cases, a new assembly procedure was necessary. A suitable length of the sheath is removed from the stub cable, to take care of the internal case wiring. The sheath is soldered into a collar which has been previously welded to the case cover plate. The cover and stub cable are inverted and secured in a fixture. Panel supporting rods are attached to the cover plate and the first panel of coils is assembled over the stub cable and supporting rods. Stub cable conductors are then fanned out between the coil units and connected to the proper coil terminals. A second coil panel is then placed in position and stub cable connections made to the coil terminals. This procedure is continued until the required number of coils has been assembled and wired. A bottom plate is then added and the assembly secured by tightening nuts on the ends of the steel supporting rods. Preformed insulators are placed over the exposed coil terminals and secured by a circumferential wrapping of twine. The cylindrical steel case shell with an insulating liner is then assembled and soldered to the cover to form an air-tight seal. The case is vacuum dried and filled with dried air for shipment.

Toll Cable Unit Cases

Lead sleeve type cases are employed for housing the new toll cable loading units in small complements. Two sizes have been standardized, 1 to 3 units and 4 to 6 units. Figure 15 shows the internal construction of the new six-unit case and of the replaced steel case for eight units. The stub cable conductors are run along one side of the assembly. Two quads branch off from the cable run and pass over the end of each unit through a space provided and fan out to the proper terminals.

Steel cases are provided to house from 7 to 48 of the new toll cable units. Although cases were available for potting as many as 80 of the superseded units, the present maximum of 48 units was believed adequate in view of the expanded use of carrier telephone systems on main cable routes. Figure 16 pictures a 12-unit case of the new design in comparison with a 14-unit case of the superseded type, and Figure 17 shows the internal construction. Six units are mounted on a circular steel panel and are locked in place by means of tabs which project from the unit containers through slots in the plate. Full complement sizes may be provided in multiples of six by use of the required number of panels. Less than full complements may be obtained by substituting empty unit containers for unwanted units. The case assembly procedure is like that described for the nonphantom coil cases, the work being done with the case inverted and the panels assembled and wired in sequence. Six steel rods fastened to the cover plate pass through holes in the panels, the entire assembly being secured by means of nuts on threaded ends of the rods. The stub cable conductors pass through the center of the case and branch out at each panel level passing over one end of each unit for connection to the unit terminals. This arrangement provides a compact and rigid structure with short, direct wiring to all units.

Conclusion

Through the utilization of improved materials and manufacturing techniques a series of coils for loading exchange area cables has been provided which exhibits the same electrical characteristics as the superseded type but employs one-third less winding copper and has considerably smaller over-all dimensions. Similarly, 3-coil units for loading toll cable have been made available which are much smaller then their predecessors and provide a 50 per cent reduction in both winding copper and core material requirements. This large reduction in core material was necessary as a war measure to conserve strategic materials. It results in approximately 25 per cent increase of resistance and somewhat increased nonlinear distortion and susceptibility to superposed direct current. Due to the reduced size and weight of the new coils and units, together with improved assembly arrangements, it was possible to reduce the size and weight of the new coil and unit cases by about 65 per cent.

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No Discussion

Trolley Coach Distribution System

G. R. McDONALD
ASSOCIATE AIEE

THE CHANGEOVER of many existing street car systems to trolley-coach operation and the installation of new trolley-coach systems have necessitated the development of a quick and easy method for the layout of the d-c distribution circuits. In this paper the system layout is based on the use of a curve which simplifies the calculations, facilitates checking various arrangements, and provides consistent results with minimum effort. At the same time, it takes into account more factors and provides more information than is readily available with other systems in common use.

The number of vehicles operated during the "rush-hour" periods determines the substation capacity, feeder size and arrangement, and circuit breaker settings. It is evident that this equipment must be of such size and capacity that during the "maximum-demand period" or "rush hour" the voltage throughout the system will permit satisfactory coach operation. Further, this equipment must be so arranged that in the event of a failure of any kind, the system will be adequately protected with a minimum amount of disturbance to the system as a whole.

In general the method is based on the use of a trolley-coach maximum current demand or "diversity curve." Rush-hour conditions only are considered and substation capacity, feeder arrangement, feeder copper and other factors are laid out to take care of these peak conditions. On any particular feeder the number of trolley coaches in operation during the rush hour can be readily estimated. The peak demand current of this number of coaches is then obtained from the diversity curve. It is evident that the substation circuit breaker must be set to carry this peak current and must also answer to the requirement of tripping on a short circuit at the end of the feeder concerned. It has been found by numerous tests that this method of procedure gives adequate operating voltage at the coaches and limits the distribution losses over the peak period to 10 per cent or less.

In order to illustrate the method used, a practical example will be worked out followed by a detailed discussion of the various factors taken into consideration by this method and features of d-c distribution systems which might be considered in making an over-all system layout.

Practical Example

In order to illustrate this method of determining feeder sizes and circuit breaker settings, a solution for the typical problem illustrated by Figure 1 will be worked out. This figure shows part of a system, including an outlying substation and a second substation assumed to be located closer to the load center or downtown section of the city. Three trolley coach routes lettered *A*, *B*, and *C* are shown. It will be noted that route *A* is a stub-end or radial route, while both *B* and *C* are internal station to station routes with multiple feeder circuits. On each route small arrows indicate the rush hour locations and direction of travel of the trolley coaches in service.

The feeder sizes for the stub end route *A* which is the simplest, will be calculated first.

Stub End—A

It will be noted that three coaches are in operation on this 10,000-foot stub-end circuit. If allowance is made for off-schedule operation, the number of coaches which might be expected on this circuit would be increased to four and for this number of coaches we find from Table II, page 3:

Expected load swing	1,157 amperes
Circuit breaker setting (stub)	1,325 amperes
Short circuit current (stub)	1,445 amperes
Line resistance (stub)	0.418 ohms

Using this value of line resistance, it is quite simple with the aid of Table III to select cable sizes that will result in the desired circuit resistance. Using Table III, page 4, the following values might be selected:

4,000 feet of 500,000 circular-mils feeder positive and negative plus trolleys at 0.03-ohms per thousand feet	=0.120 ohms
6,000 feet of 4/0 feeder positive and negative plus trolleys at 0.050-ohms per thousand feet	=0.300 ohms
Total	0.42 ohms

The value of resistance obtained is slightly over and desired, but is close enough for practical purposes.

Now the question is, "What has been done?" In brief, feeder lengths and sizes have been selected to supply current to the coaches on this line at a voltage that is within operating limits. The line losses will be somewhat less than 10 per cent. The feeder circuit breaker setting has been co-ordinated with the feeder circuit resistance so that if a short circuit occurs any place on the line, it will be properly cleared by the circuit breaker.

Multiple Feeder B

It will be noted that there is a total of six coaches shown in operation on feeder *B* between the substations. Three coaches would be carried from each substation. However, due to off schedule operation here again it is assumed that the loading on substation 1 or substation 2, feeders will amount to four coaches. From Table II it is determined that the resistance of this multiple line is 0.432 ohms. This means that the resistance of the 18,000 feet of line between these two sub-

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G. R. McDonald is in the transportation division, General Electric Company, Erie, Pa.

stations should be 0.432 ohms as although a substation circuit breaker only tends to carry half of the load on the feeder, it must clear on a short circuit in the vicinity of the adjacent substation. From Table III, page 4, it will be noted that 750,000 circular-mils feeder, together with the trolleys, has a resistance of 0.022-ohms per 1,000 feet.

18,000 feet of 750,000 circular-mils feeder and negative plus trolleys at 0.022-ohm per thousand feet	=0.396 ohm

The resistance value obtained is somewhat conservative and the feeder between these two substations on circuit *B* could be tapered somewhat toward the middle of the circuit with a consequent saving of copper. However, this is the main circuit between the two substations and serves as a back-up source of power in case one of the substations is out of service and it is not good practice to economize on copper with this type of circuit.

Multiple Feeder B with Sectionalizing Circuit Breaker at the Center

When a sectionalizing circuit breaker (not shown in Figure 1) is used in this *B* circuit, it is located at the center of the route effectively dividing the circuits into two parts. Each substation supplies power for the same four coaches and from Table II we find as before that the line resistance should be 0.432 ohms. In this case, however, the resistance value will apply to the feeder circuits from each substation to the sectionalizing circuit breaker at the center. Considering 9,000 feet of line, the resistance per 1,000 feet is 0.048 ohms and referring to Table III, page 4, the feeder sizes may be determined:

9,000 feet of 250,000 circular-mils feeder positive and negative plus trolleys at 0.047 ohm per thousand feet	=0.423 ohm

This value of resistance compares con-

Table I. Current Ratings for 30 Degrees Centigrade Temperature Rise[1]

Bare Conductors, 100 Per Cent Conductivity Copper In Horizontal Position, 40 Degrees Centigrade Air Temperature

	Size	Outside Diameter Inches	Current Rating Outdoor Amperes
Solid	0*	0.325	240
	00*	0.365	280
	0000*	0.480	380
Stranded	0*	0.373	248
	00*	0.418	286
	0000*	0.528	388
Stranded	250,000†	0.575	434
	400,000†	0.728	583
	500,000†	0.814	674
	600,000†	0.893	756
	750,000†	0.998	863
	1,000,000†	1.152	1,038

* American wire gauge.
† Circular mils.

servatively with the desired limit of 0.432 ohms required for each part of this circuit.

The resistance of the total line from substation 1 to substation 2 is 2×0.423 ohms=0.846 ohms. In the case of a short circuit at one end of the line it is necessary that the setting of the sectionalizing circuit breaker be such as to clear on 710 amperes which is the current obtained by dividing 600 volts by 0.846 ohms. A good rule of thumb is to set the sectionalizing circuit breaker at half the setting of the main substation circuit breakers. From this viewpoint the sectionalizing circuit breaker would be set at one half of 1,270 amperes which is the substation circuit breaker setting obtained from Table II. This gives a setting of 635 amperes, which supplies considerable margin below the actual minimum current of 710 amperes.

A review of the calculations for line *B* shows the following:

1. Without a sectionalizing circuit breaker the feeder must be 750,000 circular-mils and the station circuit breakers will be set at 1,270 amperes.
2. With a sectionalizing circuit breaker the feeder will be 250,000 circular-mils and the station circuit breakers will be set at 1,270 amperes.
3. The sectionalizing circuit breaker will be set at 635 amperes.

Multiple Feeder C

This line is similar to line *B*, the difference being in its length and in the number of coaches scheduled. These factors indicate that it is a line of less importance than route *B*.

There are four coaches in operation on this feeder which would amount to a load of two coaches per station. Here again for off-schedule opeartion, the number of coaches is increased to three per station and from Table II the resistance of the circuit for multiple feed is given as 0.503 ohms which amounts to 0.025-ohms per thousand feet between the two stations. From Table III, page 4, we obtain the following:

20,000 feet of 750,000 circular-mils feeder positive and negative plus trolleys at 0.022-ohms per thousand feet	=0.44 ohm

This circuit could be tapered toward the center to make a saving of copper thus:

12,000 feet of 750,000 circular-mils feeders and negative plus trolleys at 0.022-ohms per thousand feet	=0.26 ohm
8,000 feet of 500,000 circular-mils feeders positive and negative plus trolleys at 0.03-ohm per thousand feet	=0.24 ohm
Total	0.504 ohm

This amounts to 6,000 feet of 750,000 circular-mils feeder positive and negative out of each substation with 500,000 circular-mils feeder positive and negative through the center section. As this is not the main connection between the two sta-

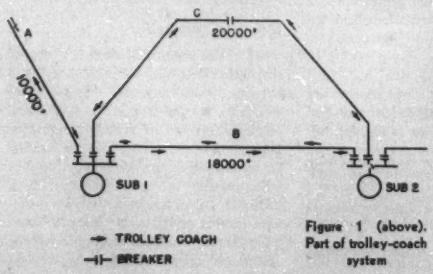


Figure 1 (above). Part of trolley-coach system

Figure 2. Typical current-time curve 44-passenger trolley coach

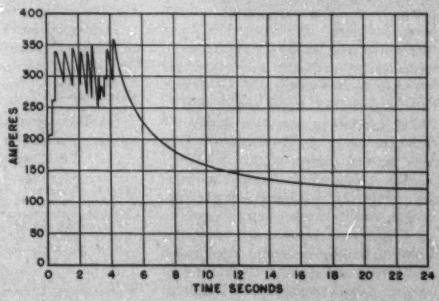


Table II. Trolley Coach Distribution System

Number Coaches	Load Swing	Circuit Breaker Set (LS×1.10) Multiple	Circuit Breaker Set (LS×1.10) Stub	Short Circuit Current (LS×1.20) Multiple	Short Circuit Current (LS×1.20) Stub	Short Circuit Resistance Multiple	Short Circuit Resistance Stub
1	450	496	320	540	895	1.110	0.667
2	771	850	1,060	925	1,156	0.650	0.518
3	998	1,090	1,250	1,190	1,367	0.503	0.438
4	1,157	1,270	1,325	1,390	1,445	0.432	0.415
5	1,271	1,400		1,530		0.392	
6	1,348	1,485		1,620		0.370	
7	1,407	1,550		1,690		0.355	
8	1,464	1,610		1,760		0.340	
9	1,480	1,640		1,785		0.336	
10	1,522	1,675		1,830		0.328	
11	1,555	1,710		1,865		0.321	
12	1,590	1,750		1,910		0.314	
13	1,630	1,795		1,955		0.307	
14	1,670	1,840		2,005		0.299	
15	1,713	1,885		2,060		0.291	
16	1,760	1,935		2,110		0.284	
17	1,815	2,000		2,180		0.275	
18	1,875	2,060		2,250		0.267	
19	1,935	2,130		2,320		0.258	
20	2,000	2,200		2,400		0.250	

tions, this tapering of the feeder circuit is considered permissible.

Multiple Feeder C with Sectionalizing Circuit Breaker at the Center

In this case, using the same loading of three coaches per station, it will be seen that the resistance of the line from one substation to the center should be 0.503 ohms which would amount to 0.0503 ohms per 1,000 feet giving the following:

10,000 feet of 4/0 feeder positive and negative plus trolleys at 0.05-ohm per thousand feet	= 0.500 ohm

From Table II we find that the station circuit breakers would be set at 1,090 amperes and by applying the above rule of thumb for setting the sectionalizing circuit breaker it is found that this should be 545 amperes.

A review of the calculations for line *C* shows the following:

1. Without a sectionalizing circuit breaker there will be 12,000 feet of 750,000 circuiar-mils feeder and 8000 feet of 500,000 circular-mils feeder positive and negative. The station circuit breakers will be set at 1,090 amperes.

2. Using a sectionalizing circuit breaker, there will be 20,000 feet of 4/0 feeder positive and negative. The station circuit breakers will be set at 1,090 amperes and the sectionalizing circuit breaker will be set at 545 amperes.

Comparative Costs

For routes *B* and *C* discussed before, two possible arrangements of feeder copper have been worked out, namely, sectionalized and unsectionalized. It is now necessary to cost the two alternative methods of installing feeder and arrive at an economical solution.

Route B Without a Sectionalizing Circuit Breaker

36,000 feet×2.320 pounds per foot× $.30 per pound	= $25,100
36,000 feet×$.09 per foot	= 3,240
Total	$28,340

It is assumed that feeder cable has a cost of 30 cents per pound and that the cost of installation and fittings is $.09 per foot regardless of feeder cable size.

Route B With a Sectionalizing Circuit Breaker at the Mid-Point

36,000 feet×0.772 pound per foot× $.30 per pound	= $ 8,340
36,000 feet×$.09 per foot	= 3,240
Sectionalizing circuit breaker installed	750
Total	$12,330

In this case the saving in the expense due to the use of the sectionalizing circuit breaker amounts to $16,010.

Route C Without a Sectionalizing Circuit Breaker

24,000 feet×2.320 pounds per foot× $.30 per pound	= $16,700
16,000 feet×1.540 pounds per foot× $.30 per pound	= 7,380
40,000 feet×$.09 per foot	= 3,600
Total	$27,680

Route C With a Sectionalizing Circuit Breaker at the Mid-Point

40,000 feet×0.6405 pound per foot× $.30 per pound	= $ 7,680
40,000 feet×$.09 per foot	= 3,600
Sectionalizing circuit breaker installed	750
Total	$12,030

Figure 3. Trolley-coach diversity curve

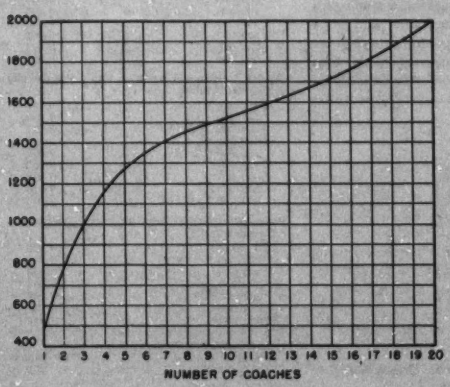
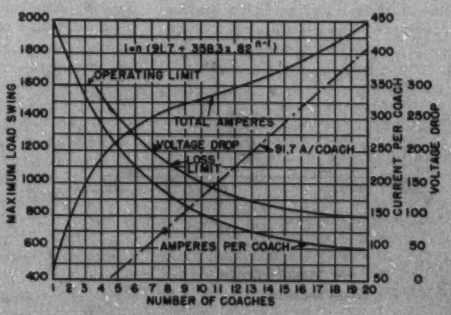


Figure 4 (below). Trolley-coach overhead



It will be noted in this case the saving by the use of the sectionalizing circuit breaker at this point amounts to $15,650.

In the foregoing discussion it was noted that the sectionalizing circuit breaker for line *B* was to be set to trip at 635 amperes and the similar circuit breaker for line *C* at 545 amperes, making a total of 1,180 amperes. If substation 1 were shut down, it would be necessary through these two sectionalizing circuit breakers to carry a total of three coaches for route *A*, three coaches for route *B*, and two coahces for route *C*. From Table II the load swing of eight coaches omitting all margins, is seen to be 1,464 amperes. It would be difficult, therefore, to operate with substation 1 shut down as the demands far exceed the setting of the sectionalizing circuit breakers.

If the two lines were sectionalized it would therefore be necessary to assume 100 per cent reliability for the equipment in substation 1; otherwise a substation failure would put this part of the system out of service. The total copper saving by the use of these sectionalizing switches is $31,660, or slightly more than the actual cost of the electrical equipment for a 300-kw unit, or about equivalent to this unit when the cost of installation, and so forth, is considered.

Feeder copper in place requires very little in the way of maintenance and has in general a very high recovery value if for any reason the feeder circuit is abandoned. On the other hand, substation equipment requires maintenance and the depreciation is very much greater than the depreciation of feeder copper. For these reasons, it is believed that substation equipment should displace or save at least twice its value in feeder copper, in order to represent an economic application. It would not therefore appear advisable to cut down on the feeder copper as above, and install additional conversion capacity in substation 1.

The feeder *B* is the shortest between the two substations and carries the heaviest traffic. With no sectionalizing, the overall resistance of the circuit as determined above is 0.396 ohm and the circuit breakers at each end of the circuit are set for 1,270 amperes. A desirable combination would therefore appear to be feeder *B* without sectionalization and feeder *C* with sectionalization. The sectionalizing circuit breaker for feeder *C* would be set at 545 amperes, making a total circuit breaker setting of 1,815 amperes for the two circuit breakers in these lines. The total load supplied by these two circuit breakers would be three coaches on route *A*, six on route *B*, and two on route *C*. The demand of 11 coaches is 1,555 amperes. The two circuit breakers should, therefore, if the load is divided in proportion to the circuit breaker settings, be able to supply this number of coaches in an emergency.

A check on the loading of the system when set up in this manner with substation 1 not in service and with the peak loading indicated on Figure 6, indicates that feeder *C* practically takes care of its own load. Feeder *B* carries its own load and most of feeder *A* load. The voltage on feeder *A* over the peak with the loading indicated will be bad. However, operation might be continued with some restrictions on rates of acceleration or increase in headways or another solution might be to use still more feeder copper for route *B*. This could be done without upsetting the economics of a single unit substation installation.

A great deal, in all studies of this kind, depends upon local conditions and the opinions of the management as to service to be rendered, and the risks to be taken.

Explanation and Comments on the Foregoing Method of Calculation

System Map and Loading

The first step in making a study of a trolley coach distribution system is to make up a system map showing each of the proposed routes. Information is then necessary on the number of trolley coaches which will be used on each of these routes over the rush hours or at any time of extraordinary loading on these routes. Knowing the schedule speed, it is then possible to spot coaches on the map. One convenient way to do this is to place a coach at the end of each line and then indicate coaches set out along the line in accordance with operating headways. If the time of layover at the end of a line is assumed to be one-half the scheduled headway, it will be seen that coaches can be spotted along the routes operating in alternating directions and spaced by an amount equal to half of the headway between coaches. When indicating the location of coaches on the map, it should be kept in mind that the schedule speed on the outlying ends of the various lines will be considerably higher than the speed obtained in downtown areas.

Substation Location

As the next step, the substations may be located on the map at what appear to be reasonable locations. These might be at the intersection of two or more lines, or at places where a feeder or feeders may be run for short distances in order to pick up more than one line. A few preliminary checks by the method outlined before

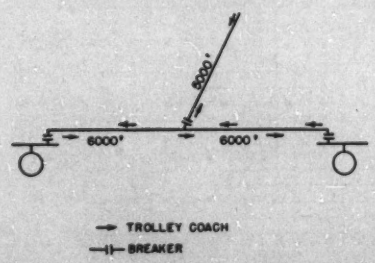


Figure 5. Branch line

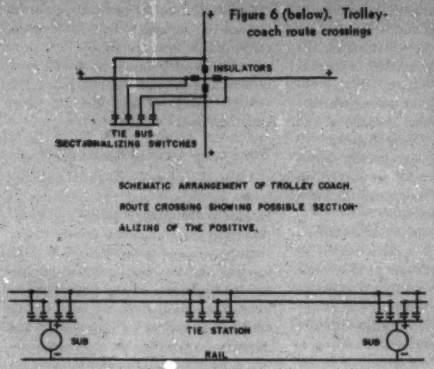
Table III. Feeder and Trolley Resistances—2/0 Trolley

	Ohms Per 1,000 Feet—Route
Trolley Only	
2/0 Trolley 80 Per Cent Conductivity	0.099
2/0 Trolley Positive and Negative	0.198
2-2/0 Trolley Positive and Negative	0.099
Feeder one side of circuit only	
4/0 Feeder	0.050
250,000	0.043
300,000	0.036
400,000	0.027
500,000	0.022
750,000	0.014
1,000,000	0.011
Feeder Positive and Negative with 2 Positive and 2 Negative Trolleys	
4/0 Feeder*	0.050
250,000	0.047
300,000	0.042
400,000	0.035
500,000*	0.030
750,000*	0.022
1,000,000*	0.018
1,500,000	0.013
2,000,000	0.010
Feeder Positive and Negative with 1 Positive and 1 Negative Trolley	
4/0 Feeder*	0.068
250,000	0.061
300,000	0.053
400,000	0.043
500,000*	0.036
750,000*	0.025
1,000,000*	0.019

1,000 feet of 1,000,000 circular-mils weighs ..... 3,090 pounds

* Preferred feeder sizes.

Figure 6 (below). Trolley-coach route crossings



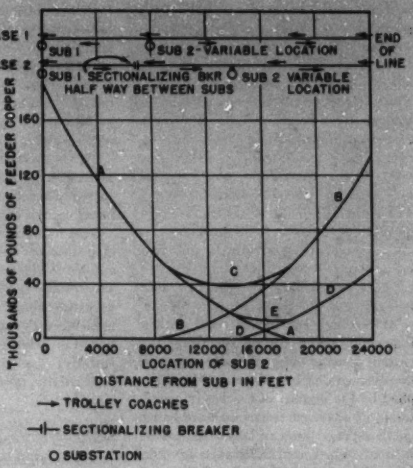


Figure 7. Location of substations along trolley-coach line

Case 1—No line sectionalizing between substation 1 and substation 2
Case 2—Line sectionalizing half way between substation 1 and substation 2
Curve A—Weight of feeder copper from substation 2 to end of line case 1 and 2
Curve B—Weight of feeder copper between substation 1 and substation 2 case 1
Curve ACB—Total weight of feeder copper case 1
Curve D—Weight of feeder copper between substation 1 and substation 2 case 2
Curve AED—Total weight of feeder copper case 2
Contact lines 2/0—80 per cent conductivity 2 way operation
Uniform feeder sections (no tapering)
Line sectionalized at substations 1 and 2

for determining feeder sizes should soon indicate whether or not the substation locations and spacings assumed are adequate. In general, for small systems, substations should be placed with respect to maintaining good voltage on the ends of the lines where the coaches are able to use the maximum speeds. Voltage in a downtown area where traffic conditions slow up the coaches, is of relatively less importance. This system will result in several substations scattered around towards the ends of various lines with comparatively long feeds into the downtown areas. With large systems the loading in the downtown areas will normally be of such magnitude as to prohibit the use of heavy feeders from remote substations and will thus dictate the location of substations at these load centers as well as toward the ends of the lines.

There can be no question as to the adequacy of the location shown for substation 1 in Figure 1, which is the outlying substation. It will be noted that this is at the junction of two routes and is a location where the system may be supplied by three feeder circuits leaving this substation.

Arrangement of Feeder Circuits

Having the location of the substations, the arrangement of feeder circuits is usually quite apparent as in Figure 1. Occasionally the circuits may become quite complicated and a number of trials making different feeder arrangements may be necessary. Feeders should, in general, be arranged to provide the maximum possible amount of sectionalization at a substation; that is, where a circuit runs past a substation the circuit should be sectionalized at such a point rather than merely running one circuit out of a substation to tap into the circuit without sectionalizing. As will be shown later, the copper saved by sectionalizing at such points is usually sufficient to offset many times the cost of additional feeder circuit breakers and circuits.

Another point which will influence the arrangement of the feeder circuits is the number of units in a substation. The cost per kilowatt of substation equipment decreases quite rapidly with an increase in the size of the units used. The cheapest installation of the substation equipment would consist of single units at each of the substations of sufficient size to carry all expected loads at that particular point on the system. Such units should, in addition, have spare capacity for helping out adjacent substations, and the copper should be installed so as to permit ready transfer of power and backup between substations. Where multiple unit substations are provided with reserve capacity, less attention need be paid to backing up this substation from the adjacent stations and, in fact, the circuits between substations might be sectionalized, or a system using stub-end or radial feeders only might be used with a very considerable saving in copper.

Diversity Curve

The system used before for obtaining feeder copper sizes depends on the maximum instantaneous power demand of a number of coaches. Data on this instantaneous demand has been plotted in the form of a diversity curve and it is from this curve that the data of Table II have been compiled. This is one of the most essential parts of the method of trolley coach system layout described herewith.

The current demand of a 44-passenger trolley coach plotted against time is illustrated by Figure 2. This shows that the peak demand for traction from one coach might be considered to be on the order of 350 amperes during initial acceleration. It will be noted, however, that the duration of this peak is only some five seconds when the coach is operating on level profile. When a number of

coaches are operating on a circuit the probability of all coaches starting simultaneously becomes progressively less as the number of coaches is increased. This effect is shown by the curve of Figure 3. The total expected ampere demand of various numbers of coaches is shown plotted against the number of coaches in operation.

At the low end of the curve it will be noted that one coach is indicated as taking 450 amperes. This is larger than the current values given by Figure 2 but includes margin for starting the air compressor simultaneous with starting the coach. This was found to occur quite frequently in practice.

At the high end of the curve with a large number of coaches operating, the current demand obviously must approach the average demand of one coach multiplied by the number of coaches in operation. The current values used are based on an average figure of 50-kw per coach, with a 10 per cent allowance for line losses during the rush-hour period under consideration. The curve therefore approaches 91.7 amperes per coach with large numbers of coaches in operation.

Fifty kilowatts per coach may be considered a relatively high value. It should, however, take into consideration difficult operating conditions as experienced during rush hours and also adverse weather conditions.

The curve is plotted in accordance with the empirical formula:

$I = n\,(91.7 + 358.3 \times 0.82^{n-1})$

I = total current and n = the number of coaches

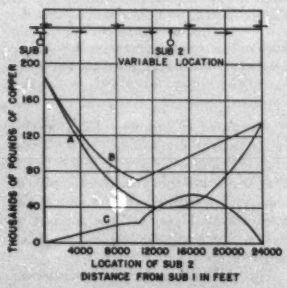


Figure 8. Result of not sectionalizing at a substation

Curve A—Amount of copper required with line sectionalized at substation 2

Curve B—Amount of copper required with line not sectionalized at substation 2

Curve C—Difference between A and B showing amount of copper saved by sectionalizing line at substation 2

It will be noted that the 91.7 corresponds to the average current and is the ultimate value which would be obtained on the curve for a very large number of coaches in operation. The figure 358.3 is obtained by subtracting the average current 91.7 from the maximum demand of one coach which is taken as 450 amperes.

The factor 0.82^{n-1} is arbitrarily chosen to reduce the load swing portion of the current taken by the coaches as the number of coaches is increased.

This formula is included as an aid in case this method of system layout is applied to different vehicles or to systems which have special operating conditions departing from the average. The values used herewith are based on test data obtained on several systems and are representative of average conditions of feeder conductivity, profile, schedule speeds, and other factors.

Feeder Circuit Breaker Settings

A stub-end feeder is one which receives power at one end or one point only. A multiple feeder is one which receives power at two or more points. When dealing with a small number of coaches, care should be taken to select values from Table II for the proper type of operation.

For any feeder circuit, the number of coaches for normal scheduled operation over the peak period may be determined from the map or otherwise. The expected maximum demand of this number of coaches may readily be obtained from the diversity curve of Figure 2. The feeder circuit breaker should then be set with some margin above this expected demand. A short circuit at the end of the line should then give in turn some margin in current over the setting of the circuit breaker. Table II gives a listing of these data for up to 20 coaches. The load swing current of the second column is increased by 10 per cent to arrive at the circuit breaker setting as given in column 3 and by 20 per cent to arrive at the short circuit current of column 4. The over-all resistance from the substation to the end of the feeder in the case of stub end, to the adjacent substation in the case of multiple feed, or to the sectionalizing circuit breaker if used, is determined from the substation bus voltage (taken as 600) and the short circuit current.

Feeder and Trolley Resistances

Table III gives convenient reference tables for determining the resistance of various combinations of trolley and feeder circuits. This tabulation covers trolley circuits alone and trolley circuits for both one-way and two-way operation, supplemented by various sizes of feeder conductors. The values given are total for the circuit and comprise both the positive and negative sides of the circuit. The tabulation is based on 2/0 trolley wire of 80 per cent conductivity. If other sizes of trolley wire or different conductivity is used, other tabulations may be readily prepared.

Feeder Tapering

Table II indicates the limiting value of resistance which should be used for any feeder with a given number of coaches in operation on this feeder circuit. The minimum weight of feeder copper is obtained when this feeder circuit is made up of a uniform cross section from end to end. On the other hand, minimum losses would be obtained if the equivalent cross section of the conductors, trolley and feeder, were varied in proportion to the square root of the current in each part of the line. This would lead to the idea that where small numbers of coaches are in operation on a particular feeder, a uniform cross section of copper should be used from end to end of the feeder as the losses involved will be a comparatively small factor as compared to the investment in feeder copper. On the other hand, where a comparatively large number of coaches are in operation on a particular feeder, the cross section might be varied in proportion to the square root of the current in

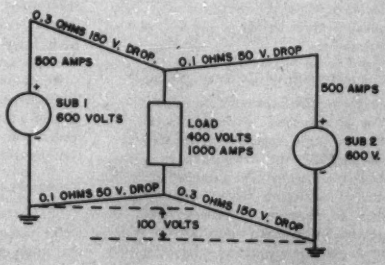


Figure 9. Difference in ground potentials with an unsymmetrical arrangement of feeders

order to achieve minimum losses as losses on a heavy feeder might become an important item. This rule would, of course, be varied somewhat by the cost of a feeder installation; that is, if it were possible toward the end of a line to eliminate feeder altogether, this might be advisable and would justify installing somewhat heavier feeder over other parts of the line.

Operating Voltage

As noted before, the power consumption of a trolley coach is taken as 50 kw. If this value is increased by 10 per cent to 55 kw to make allowance for line losses, then the system should be so designed so that the average line drop to the trolley coach should not exceed 10 per cent with the coaches taking 91.7 amperes each. The average line drop is two-thirds of the maximum average drop. In other words, when the coach is at maximum distance from the point of power supply, its average operating voltage drop should not exceed 15 per cent based on 91.7 amperes per coach. The instantaneous drops will, however, greatly exceed the average and should be based on ratio of the maximum expected instantaneous current demand per coach, as determined from the diversity curve, to the average current per coach.

Referring to Figure 4, it will be noted that the maximum instantaneous ampere swing per coach when 20 coaches are operating is 100 amperes. The allowable maximum instantaneous drop during this load swing would then be $600 \times 0.15 \times 100/91.7$ or 98 volts. Similarly, for 10 coaches the maximum instantaneous drop may be determined as being $600 \times 0.15 \times 152.2/91.7$ or 250 volts. These values are shown plotted on Figure 4 against the number of coaches and illustrate the maximum allowable drop which should be permitted.

It will be noted that with a small number of coaches operating, this curve would tend to give a drop of more than 300 volts instantaneously. The minimum operating voltage of the trolley coach is considered to be 300 volts. The curve is therefore terminated at this value and a line drawn through the range of one to three coaches, giving 300 volts as the maximum allowable instantaneous drop.

When a small number of coaches are operating on a stub-end feeder and the instantaneous drop is not to exceed 300 volts, it is necessary to modify the resistance of the feeder circuit as indicated for stub feeders in Table II. In other words, if one coach has a load swing of 450 amperes and the maximum allowable drop with this load swing is 300 volts, then the resistance of the line will be 0.667 ohms as given in the tabulation. This value permits higher short-circuit current than is obtained on a multiple feeder with one coach and will permit correspondingly higher settings of the substation circuit breakers. Table II has been modified in the range of one to four coaches when operating on stub-end feeders in order to adjust the resistance values in this manner.

When the resistance of the feeder circuit is determined by the methods shown herewith, it has always been found that the operating voltage at the coaches is better than the limiting values indicated on Figure 2. It is therefore not unusually necessary to check voltage drops when laying out the feeder copper.

Voltage Check

The foregoing description of the method for obtaining the voltage drop curve of Figure 4 is somewhat involved due to the use of average maximum and maximum instantaneous voltages. If the voltage curve is accepted, a check on the layout of Figure 1 will indicate its application.

Consider feeder A with normal conditions, as shown by Figure 5. Three coaches are shown, one at the end of the line, one at the halfway point, and one at the substation. From Table II the demand of three coaches is noted to be 998 amperes or 333 amperes per coach. Using these currents and the resistances associated with the foregoing locations, the voltage drop at the end coach will be found to be 196.5 volts. From Figure 4 the maximum drop permitted is 300 volts.

Assume more severe conditions for feeder *A* such as four coaches. The maximum demand current will be 1,157 amperes, 289 amperes per coach. Using uniform spacing for the coaches as before the drop is found to be 223.6 volts. From Figure 2 the allowable drop for four coaches is found to be 285 volts.

For the feeder *B* without sectionalizing which normally supplies six coaches, consider the worst possible condition of four coaches supplied from one substation with no help from the other and these coaches located, one at the center, one 6,000 feet from a substation, one 3,000 feet, and the fourth at the substation. Using 289 amperes per coach and the above distances the maximum drop is found to be 114.5 volts. The allowable drop is 285 volts.

Considering the worst possible conditions for feeder *C* with a sectionalizing circuit breaker, one coach may be located at the sectionalizing circuit breaker one-half way between the circuit breaker and a substation, and one at a substation. Under these conditions we find 242-volts drop which is well within the 300-volt limit.

Figure 10. Multiple type of sectionalizing switch

From these voltage drops it is apparent that stiff operating voltage is always obtained with multiple feeders which are not sectionalized. Good voltage from the standpoint of line losses and average voltage is obtained with stub end and sectionalized multiple feeders.

Substation Units

Having completed a feeder study with substation locations assumed as described before, it is an easy matter to arrive at the capacity required in the substation units. By inspection of the map, the number of coaches taking power from a particular substation may be readily determined within fairly close limits. Some allowance should be made for off-schedule operation and any future growth or other factors which might influence the selection of substation units should be considered. The total number of coaches determined in this way multiplied by 50 kw per coach with a 10 per cent allowance for line losses will give the average load for the rush-hour period. The corresponding load swing may be readily determined from Figure 3.

All conversion equipment for transportation service is rated 100 per cent load continuous followed by 150 per cent load for two hours. The equipment may have a one-minute load swing rating of either 200 per cent for one minute or 300 per cent for one minute. The average rush-hour load as determined before should come within the 150 per cent rating of the unit selected. The load swing rating as deter-

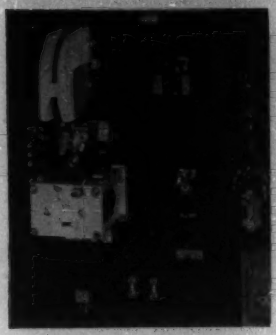


Figure 11. Stub-multiple type of sectionalizing switch

mined from Figure 2 should approximate the load swing rating of the conversion unit.

An example of this procedure will be obtained by referring back to Figure 1. From the layout it will be noted that substation 1 over this peak period supplies power to eight coaches. If we assume 50 kw per coach with 10 per cent line loss, then this average demand is 440 kw. This could be carried by 300 kw units having a 150 per cent two hour rating of 450 kw. Also from Figure 2 it will be noted that the momentary load swing given by eight coaches is 1,464 amperes, equivalent of 878 kw. This 300-kw unit would therefore require commutating ability for at least a few seconds to take care of this expected load swing of approximately 300 per cent. If the conversion unit commutation is limited to 200 per cent it would be necessary to consider the next largest size of 500 kw.

No allowance has been made for off-schedule operation and it is noted that the requirements are very close to the rating of a 300-kw unit. In this case it might therefore be well to consider the next largest size of unit which would be 500 kw. Another factor which bears on this choice would be the fact that the cost per kilowatt of a 500-kw unit is very much less than that of a 300-kw unit. For the electrical equipment of the substation only a 2/3 increase in kilowatt capacity may be obtained with approximately 1/3 increase in the cost of the equipment. This is true because the a-c switchgear, the control equipment and the direct-current feeders would be essentially the same for either unit. In addition, the installation cost, building and land would be practically identical.

For small substations such as considered before it is obvious that in the interest of economy single units should be considered. There is not much risk in this if modern rectifying equipment is considered as the availability of this type of substation equipment is over 99 per cent. In addition, arrangements should be made in the feeder layout to provide for emergency operation with one substation out of service. This may very readily be accomplished where direct feeders can be arranged between several substations. Even in Figure 1 where only two substations are involved, a fairly good arrangement for emergency operation with substation 1 shut down has been obtained in the arrangement of the feeders.

General Comments

System Negative

It is customary to sectionalize only the positive side of a trolley coach system. The negative is permanently interconnected. In many cases it is necessary to run feeder cables for some distance out of a substation to reach the route being supplied. In such cases a compromise on the amount of negative feeder may be made. The positive circuits will all have to be separate and of a size to limit the voltage drop. The negatives of several circuits may be combined with a compromise size of cable as the diversity effect of the load will assist in limiting the voltage drop.

Branch Routes

Figure 5 shows a trolley coach route between two substations with a branch route leaving at a point about half way between the two substations. This presents an awkward situation to handle, as it is difficult to clear a short circuit on the branch route without resorting to an extraordinarily large amount of feeder copper.

Referring to Figure 5, it will be noted that there are eight coaches in operation. It is assumed that the substation circuit breaker at each end of the section would be set to carry five coaches. The short circuit current would be 1,530 amperes; the resistance between substations 0.392 amperes. The solution for the line between substations might be made as follows:

12,000 feet of 500,000 circular-mils feeders and trolleys at 0.03 ohms per thousand feet	= 0.360 ohms

Assume a short circuit at the end of the branch line and no circuit breaker in the branch feeder. This short circuit must trip the circuit breakers at both substations in order to clear. The resistance of the circuits from the two substations to the mid-point where the branch leaves is 0.090 ohms. The current necessary to trip the substation circuit breakers would be twice the short circuit current given before, or 3,060 amperes. The resistance of the branch circuit should be 600 divided by 3,060 minus 0.090 or 0.106 ohms, which would amount to 0.021 ohms per thousand feet and would require 1,050,000 circular-mils feeders.

10,000 feet × 2.320 pounds per foot × $.30	= $6,960
10,000 × $.09 per foot labor	= 900
Total	$7,860

If a stub-end reclosing outdoor type of feeder circuit breaker is installed at the junction between the branch line and the main line, the circuit breaker would be set to carry three coaches and the total resistance from the supply point to the end of the branch line should be 0.438 ohms. Deducting 0.09 ohms for the resistance from the substations to the mid-point leaves 0.348 ohms for the branch line, or about 0.069 ohms per thousand feet.

3,000 feet of 4/0 feeder and trolley at 0.05 ohms per thousand feet	= 0.15 ohms
2,000 feet of trolley only at 0.099 ohms per thousand feet	= 0.198 ohms
Total	0.348 ohms

The cost of this feeder arrangement on the branch line is as follows:

6,000 feet × 0.6405 pound per foot × $.30	= $1,155
6,000 feet × $.09 per foot labor	= 540
Sectionalizing circuit breaker	750
Total	$2,445

The saving by use of the outdoor sectionalizing circuit breaker is $5,415.

Route Crossings

A confusing situation is sometimes presented by route crossings at points where it would not be desirable to install a substation. If the various circuits are all joined together at the point of crossing, a short circuit on any one route is difficult to isolate as although the substation circuit breaker directly feeding the short circuit may open, the current fed from remote points would be divided among a number of circuit breakers with the possibility that the trip point of these various circuit breakers would not be reached. This is somewhat the same condition as discussed

before under the subject of Branch Routes, where a short circuit on the branch might be evenly divided between two substation circuit breakers and thus require a very large amount of short-circuit current to insure tripping.

Figure 6 shows a possible solution to this difficulty which involves the use of outdoor sectionalizing switches for connecting the various circuits together. Each of the four circuits shown might be fed from a different remote substation. However, in effect, the situation is exactly the same as that obtained with a two-track railroad, which is also shown in this illustration. If there is a short on any route, current to this short circuit would be fed into the tie bus through three circuit breakers and this total current would be fed out through one circuit breaker to the short circuit. This would lead to the idea of using polarized circuit breakers, but this is hardly necessary as the circuit breaker which should open always carries the sum of the current of the other three circuit breakers.

Besides affording a ready means of sectionalizing the system at this point, the installation of sectionalizing circuit breakers results in making maximum use of the copper of the various circuits insuring minimum losses and minimum voltage drops.

For reclosing the tie circuit breakers, exactly the same scheme as used in the main line railroad sectionalizing station could be followed, that is, the operating potential for each of the circuit breakers could be obtained from its associated feeder. Then, as soon as this feeder is energized at the substation end, voltage would appear at the tie station and the associated circuit breaker would be closed.

One or more of these feeders might be of the stub-end type without changing this method of approach.

Substation Spacing

Figure 7 illustrates an interesting study on the location of two substations along a trolley coach route. Ordinarily it is considered that with distributed loading along a single line, the substation nearest the end of the line should supply a stub end of the system which would be half of the distance between substations. This is so if considered with a uniform copper section throughout the line and considered only from the standpoint of voltage drop. Figure 7 shows two cases which have been studied.

Case 1 shows a section of trolley coach route 24,000 feet long with seven trolley coaches spaced as indicated. This situation was studied from a theoretical standpoint using odd copper sizes and considering uniform section of feeder copper throughout each part of the system. If the complete line were supplied from substation 1, it will be noted that 185,000 pounds of feeder copper would be required. This feeder copper may be reduced by installing a separate substation indicated as substation 2. It is interesting to note that without any sectionalizing in the line between substations as indicated for case 1, to obtain minimum copper the second substation would be spaced at approximately 58 per cent of the distance from substation 1 to the end of the line.

Case 2 illustrates the effect of installing a sectionalizing circuit breaker between the two substations. In this case the sectionalizing circuit breaker may be set to trip at half of the value used for the substation circuit breakers, and the resistance of the line between the two substations may be increased as it is not necessary for the substation circuit breaker to trip on a short circuit any farther away than half of the distance between the two substations. Under these conditions, using exactly the same loading, it is found that the total amount of feeder copper for the system may be very considerably decreased and the most economical location for the second substation is two-thirds of the distance from the first substation to the end of the line.

In Case 2 with a sectionalizing circuit breaker between the substations and the copper section reduced to a minimum, it is obvious that the outlying substation, substation 2, must have complete reliability as it would not be possible to handle the traffic indicated from substation 1 only. Line drops would be excessive and the low setting of the sectionalizing circuit breaker would also make operation without substation 2 prohibitive. This would probably indicate the installation of two units in substation 2, either of which would be capable of supplying all of the load demand. If a greater amount of copper were used between the substations, then the two substations would provide mutual support and the sectionalizing circuit breaker half way between the two substations would not be required. In this case, the additional copper could very readily be offset by reduced investment in substation capacity.

Result of Not Sectionalizing at Substations

In many cases it is common practice not to sectionalize the distribution circuits at substations particularly where the substation is near the end of the particular line. Figure 8 illustrates that this practice may be extravagant of copper if the circuits are laid out to provide isolation of short circuits. Curve A of this figure is the same as Curve ACB of Figure 7, and gives the total weight of copper required as the location of substation 2 is varied over the length of the line. The line in this case is sectionalized at substation 2. Curve B shows the weight of copper required if the line is not sectionalized at station 2. Curve C shows the difference between curves A and B or the saving in copper obtained by sectionalizing.

Feeder Location with Rectifier Power Supply

It is customary to carry the positive and negative feeders of a trolley coach system on one side of a street, as this can be done by using one cross arm. The idea of carrying the positive and negative feeders on opposite sides of a street has been discussed. This would be more expensive and in addition if the power is supplied by rectifiers, might be somewhat objectionable. The d-c output of a rectifier contains harmonics which may cause telephone interference if there is magnetic coupling between the d-c power and telephone circuits. Short distance between positive and negative circuits reduces the chances of having this type of trouble.

Grounding Practice

Many different practices are followed in grounding trolley coach distribution systems. Some systems operate without grounds of any kind, others operate with the negative grounded at each substation.

Figure 12. Typical substation

A compromise between these two used by some companies is to have the negative permanently grounded at the base load or most important substation. The negative at other substations is then grounded only when these substations are in operation and are connected to the line.

With the negative grounded at the station only, it is customary to use lightning arresters at intervals along the distribution circuits connected from both the positive and negative circuits to ground.

With the negative permanently grounded at each substation, it is possible to have ground current between substations when a substation, between the load and its source of power, is not in service. The compromise scheme of a permanent ground at one substation and grounds at other substations only when they are in service, would appear to have several advantages. Ground current between substations should be eliminated. Lightning protection for the substation equipment and safety of operating personnel should be the equivalent of that obtained at substations having the negative permanently grounded.

Unequal Ground Potentials

Where grounds are used at substations, care should be exercised to prevent differences of potential between the grounding points. One cause of such differences might be an unsymmetrical arrangement of positive and negative circuits as illustrated in Figure 9. The constants chosen for this illustration are such as to exaggerate the effect. The same thing is, however, possible with distributed loading. Such an unsymmetrical arrangement might be introduced when a changeover is made from street car to trolley coach operation.

Bare Conductor Heating

It is sometimes necessary to check the current carrying capacity of bare conductors. These data shown in Table I, are not readily available, and are therefore included for easy reference.

As noted before, there are many different approaches to this problem of trolley coach system layout and any method used necessarily involves arbitrary assumptions and approximations.

It is believed that the system of layout described herewith has considerable advantage as it provides a quick, easy approach which takes care of

1. Proper operating voltage.
2. Feeder circuit breaker settings.
3. Isolation of short circuits.
4. Necessitates close attention to the arrangement of feeder circuits.
5. Facilitates quick checking of over-all costs with various substation locations and arrangements.
6. The cost of a layout obtained by this method will not be more than that of a good layout obtained by other methods. Actually the cost will probably be less due to the ease of checking various arrangements.

This method of system layout in its present form has been in use for somewhat over three years and even before this time was used in principle from time to time as the occasion demanded. It is believed that sufficient experience has been obtained to permit recommending the use of this procedure on all trolley coach power supply distribution problems.

Reference

1. Heating and Current Carrying Capacity of Bare Conductors for Outdoor Service. O. R. Schurig, C. W. Frick. *General Electric Review* (Schenectady, N. Y.) 1930.

Discussion

S. M. Dougherty (Westinghouse Electric Corporation, East Pittsburgh, Pa.): McDonald's paper on trolley coach distribution system contains a very interesting approach to the problem of laying out the feeders and substations for a trolley coach system. Considerable experience and thought must have entered into the preparation of the various tables and curves which are used to determine the system components. It will be found, however, that the data presented do not consider at least one feature which would permit further economies in feeder copper. This is the use of short circuit detector equipment instead of straight overload protection.

When short circuit detector equipment is used the circuit breaker is tripped only when a fault occurs on the particular feeder or trolley. On the other hand, legitimate loads do not trip the circuit breaker. This is true even though the current which results from a fault at the end of a long feeder may be considerably under that required to accelerate coach loads concentrated closer to the substation. Because of this ability to discriminate between legitimate load swings and current rate of rise resulting from a fault anywhere on the feeder, the short-circuit detector system provides the greatest continuity of service as well as maximum economy in the amount of feeder copper required.

As applied to an ordinary feeder, the short circuit detector scheme is made up of three items; an impulse coil which supplies the tripping intelligence, a polarized instantaneous detector relay which functions when the fault is near the substation and a short circuit selector relay which operates for faults beyond the range of the instantaneous relay.

In effect, the impulse coil is similar in construction to a through-type current transformer except that it is specially designed so that it will not be saturated either by normal direct current load current or by current resulting from a short circuit in the feeder. As long as the feeder current remains constant, there is no voltage induced in the secondary winding of the impulse coil. When the feeder load changes, for example due to a coach starting, the resulting flux change in the impulse coil will cause a current to flow through the short circuit detector and selector relays which are connected to the impulse coil secondary. However, the rate of change or the magnitude of this load current will not circulate enough secondary current to operate either relay. When a fault occurs near the substation, the short circuit current will build up to its maximum value in a comparatively short time. This rapid change in current will produce a high current peak of short duration in the impulse coil secondary. This current flowing through the high-speed short-circuit detector relay will be sufficient to cause it to operate and trip the feeder circuit breaker. If the fault is at or near the end of this feeder, there will be a greater ratio of inductance to resistance in the circuit. Because of this, the rate of increase and the final value of this fault current will not be as great. As a result, the impulse coil secondary current will be of longer duration but the peak value will be less than that resulting from a short circuit near the substation. This peak will not reach a high enough value to operate the short circuit detector relay but the duration will be long enough to cause the selector relay to close its contact and trip the circuit breaker. It should be noted that the short circuit selector relay is set to operate at a value considerably under the detector relay setting, but its inherent time delay will prevent tripping on faults near the substation or on the impulse peaks which result when one or more coaches are accelerating. This method of short circuit detection has been in use for a number of years on various types of transportation systems and has given excellent service.

Twenty-Five Years of Copper–Copper Oxide Rectifiers

L. O. GRONDAHL
FELLOW AIEE

WHEN it was decided to do the copper-oxide rectifier the honor of celebrating the 25th anniversary of its origin with a session on the subject, the choice of the date obviously had to be arbitrary. The first discovery of the rectifying phenomenon that exists between cuprous oxide and the copper on which it is formed was made in November, 1920, 27 years ago. The first practical use of the rectifier was made in 1924, 23 years ago. The average of the two figures is 25 years, so the date seems appropriate. The writer has been assigned the privilege of reviewing the history.

Discovery and Early Development

The discovery of the rectifying characteristic of a copper-cuprous oxide junction was a by-product of another investigation, which at that time was of interest in railway signaling. There existed among railway signal engineers a conviction that relays with moving parts could be improved upon by substituting something that would function in the same way without the movement of parts and without contacts that had to be closed and opened mechanically. The obvious procedure was to study all the different physical devices which had impedances that could be changed at will, with the hope that by the use of such devices and by changing impedances from high to low and vice-versa as required, it would be possible to provide the equivalent of opening or closing an electric circuit by means of contacts. In this effort a study was made of the use of vacuum tubes, gas-filled tubes, light sensitive cells, heat sensitive cells, and variable reactors in circuits that could be substituted for the standard signaling circuits using electromechanical relays. It was found possible on paper to set up such equivalent circuits and many of them were drawn in detail.

For reasons that need not be discussed here, light sensitive cells seemed at one time likely to be the most practical, at least if they could be made with a very low illuminated impedance. The two light sensitive materials that seemed most likely to be useful were selenium and cuprous oxide. Selenium seemed less promising because its behavior up to that time had been found in the laboratory to be very erratic, and to depend on many conditions of heat treatment. Cuprous oxide on the other hand had been found to be more consistent in its behavior.

It was realized that in order to get a cell of sufficiently low impedance to provide a circuit to energize lamps or other equipment, it would be necessary to use a path in the cuprous oxide which is short and of great cross section, since cuprous oxide is a semiconductor of high resistance compared to metals. This consideration led to the conclusion that it would be necessary to conduct the useful current across a thin sheet of cuprous oxide. Cuprous oxide is a brittle material, and would be difficult to produce and to use in thin sheets. This led to the very fortunate expedient of producing the cuprous oxide on a sheet of copper by oxidation and to stop the oxidation when there was still enough copper left to serve as a support for the sheet of cuprous oxide, and at the same time to serve as a contact to one side of the semiconductor.

The first unit was made by placing strips of copper in a nichrome furnace operated at temperatures between 900 and 1,000 degrees centigrade and oxidizing for approximately an hour. Since it was feared that rapid cooling would crack the oxide, the cooling after oxidation was made slowly. During the cooling a certain amount of cupric oxide formed on the surface of the cuprous oxide. The unit was prepared for the photoelectric experiment by breaking off the cuprous oxide at one end of the strip to make it possible to make connections with the copper. The cupric oxide on the surface was ground off on an emery wheel and a contact was made to the free surface of the oxide by laying a piece of lead against it and pressing the surfaces together by means of a clamp. See Figures 1 and 2.

The unit was then put in an ordinary Wheatstone bridge circuit to determine its resistance and to determine the effect of illumination on that resistance, with the hope that illumination would drop its normally high resistance to such a value that reasonable amounts of current could be passed through it without the use of excessive voltages. In the Wheatstone bridge the unit had a resistance of 400 ohms, and the illumination of the unit had no observable effect on its resistance. This seemed strange so the experiment

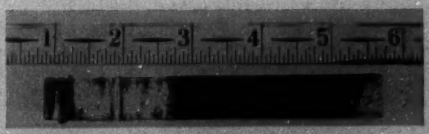


Figure 1. The first copper oxide rectifier

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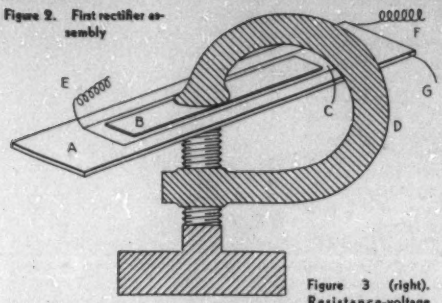
L. O. GRONDAHL is consultant in research, Westinghouse Air Brake Company and Union Switch and Signal Company, Swissvale, Pa.

It is not possible here to list all of those who have contributed to the development of the copper oxide rectifier. Very substantial contributions have been made by Dr. P. H. Geiger and Dr. P. H. Dowling.

Figure 2. First rectifier assembly



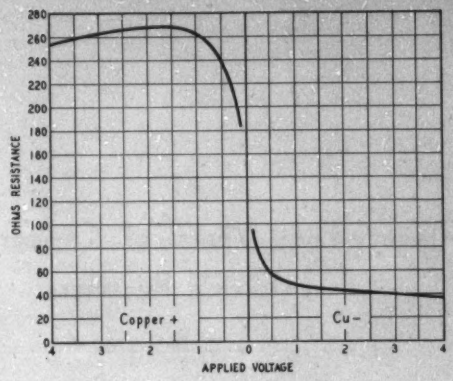


Figure 3 (right). Resistance-voltage characteristic of first rectifier measured in new assembly nine years after oxidation

was repeated under varying conditions for several days. During this experimentation it was found that sometimes the resistance as shown by the bridge was 400 ohms, and at other times it was 1,200 ohms. The existence of these two definite values of resistance was a puzzle until it was discovered that the resistance depended upon the polarity of the battery used to energize the Wheatstone bridge or similarly on the way in which the copper oxide unit was connected into the Wheatstone bridge. This immediately looked like a rectifier, but it was not realized fully at first that even such a ratio might be useful in some rectifier applications, Figure 3. The unit was laid aside for a while on account of the urgency of the original problem, namely the finding of a suitable relay without moving parts.

Some months later there arose the need of a rectifier to charge a small *B* battery. There was also needed a rectifier for use on a locomotive in train control apparatus to operate a d-c relay from an a-c source. In such applications the small rectifiers that were available at that time were not very suitable. There were, for instance, the aluminum wet rectifier, rectifiers using vibrating contacts, and other similar cumbersome equipment difficult to maintain, and difficult to use under any condition and especially so under the conditions existing on a locomotive. It seemed likely that the copper–cuprous oxide combination that had been observed some time before might be developed into a useful rectifier for these purposes, and the attempt was made at once. By the fall of 1921 the rectifier began to show promise. At that time it had been concluded that the practical way to assemble such units and to put many elements in series or in parallel was to make the units in the form of washers and to assemble them on bolts with the necessary connectors and insulators.

In these early experiments there was a bewildering mass of details that had an effect on the result, and on the usefulness of the device. Nothing was known about suitable voltages, current densities, type of copper to use, heat treatments, methods of making contact to the free surface, and many other details too numerous to list now. In spite of this the unit became useful partly because the competitive units available at that time were very poor. Efficiencies of about 20 per cent and very short life was the best that could be had in the existing equipments. By 1924 the first practical installation was made. At that time the rectifier was used in a train control equipment on the Delaware, Lackawanna and Western Railroad to operate a d-c relay from an a-c supply. The rectifier is still in use in that same application.

The subsequent growth of its usefulness was rapid and by the fall of 1926 it had gone into production at the Westinghouse Electric Corporation's plant for use as an *A* battery charger, since this was before the advent of the vacuum tube with an indirectly heated cathode. During the latter part of 1926 this production rose to 6,000 *A* battery chargers a day. In 1926 the development of the rectifier was announced at a meeting of the American Physical Society.[1] A proposed theory for the copper oxide rectifier was published in that same year.[2]

Since that time the cuprous oxide rectifier has enjoyed a steady growth in three phases:

1. Development of theory.
2. Improvement in quality of the rectifier and in variety of characteristics and of applications.
3. Steady increase in production in all industrial parts of the world.

Theory

We shall not have time here to go into a full discussion of all the theoretical suggestions that have been made to explain the performance of a copper oxide rectifier. These suggestions range from a geometric arrangement of parts through thermoelectricity, electrolytic action, work functions, the assumption of the existence of an insulating layer, and even to the assumption of a physical separation between the oxide and the mother copper to a combination of many of these, with the assumption of the presence of an electron atmosphere in the cuprous oxide.

The physical facts that have to be explained may be listed as follows:

1. The asymmetric impedance is found to be concentrated at the junction between the mother copper and the cuprous oxide.
2. When voltages applied approach zero, the resistances in opposite directions approach equality.
3. It follows at once that the asymmetry in resistance depends on the voltage applied, and is not inherent in the unit in its normal stabilized condition.
4. In the low resistance direction and at low voltages the impedance decreases approximately exponentially as the voltage increases. At higher voltages the low resistance becomes nearly constant.
5. In the high resistance direction the impedance increases with increasing voltages up to a very high value at approximately $1^1/_2$ volts beyond which it decreases with increasing voltage at a rate that is nearly linear.
6. It is found that when the areas are substantially equal the rectification always takes place at the junction even when the contact on the free surface is made with

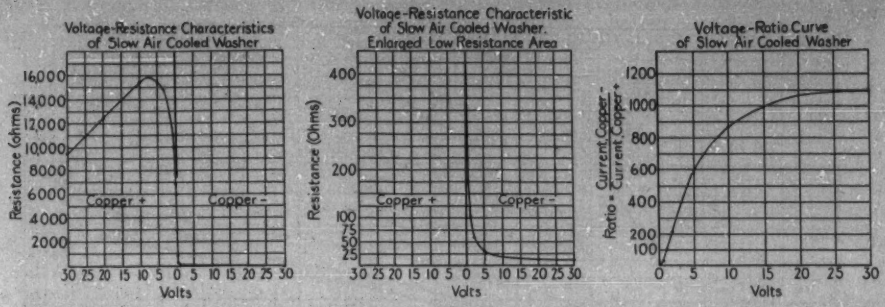


Figure 4. Resistance-voltage characteristics and resistance ratio of air cooled disk (area 10 square centimeters)

copper, so that the cuprous oxide lies between two copper contacts.

7. The last observation in common with many others indicates that the type of contact or junction between the cuprous oxide and the mother copper is quite different from ordinary solid contacts.

8. There is a considerable electrostatic capacity at the junction between the copper and the cuprous oxide.

9. Except for this capacity the impedance in both directions is a pure resistance.

10. The asymmetric response to applied voltages is instantaneous.

11. Illumination of the junction produces a photoelectromotive force.

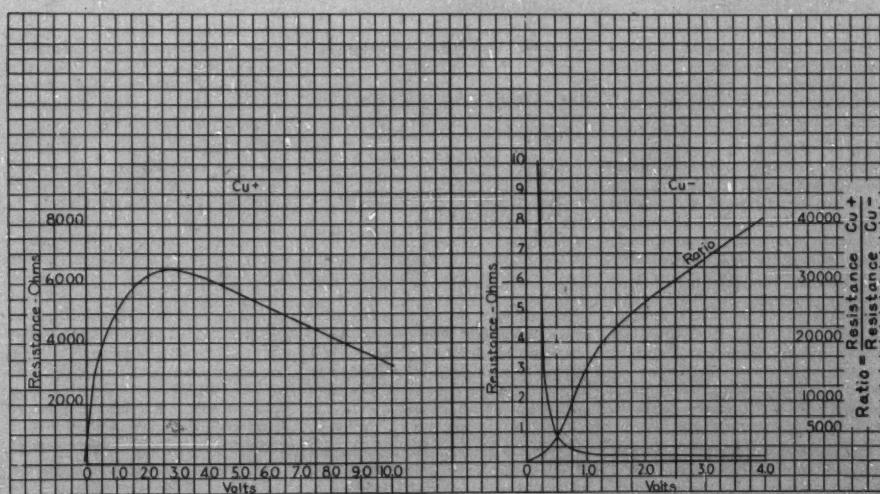
A theory that was proposed in 1926 took these facts into consideration, and while it is very crude in view of more recent developments in the theory of semiconductors it, nevertheless, contains the fundamental element of an electron atmosphere in the cuprous oxide which is also in principle a part of the present more sophisticated theory.[4] A discussion of these theories will be included in the oral presentation.

Practical Development

The copper oxide rectifier has enjoyed one condition that was ideal for the development of a new tool. At the beginning competitive devices were so unsatisfactory that even the crudest type of copper oxide rectifier found useful applications. It was not necessary, therefore, for the copper oxide rectifier to come into the market at the beginning in its most highly developed form. As the development proceeded, new applications became possible so that there was at all times in the commercial situation encouragement for further development. The novelty of the device was well illustrated by the attitude of the United States Patent Office. It became necessary in the early prosecution of the patent applications to provide evidence in the form of affidavits executed by competent engineers, testifying to the performance of the device.

The making of the first units has already been described. In the attempt to develop the process to produce better rectifiers, many conditions, each one of

Figure 5. Voltage-resistance characteristics of water quenched washer



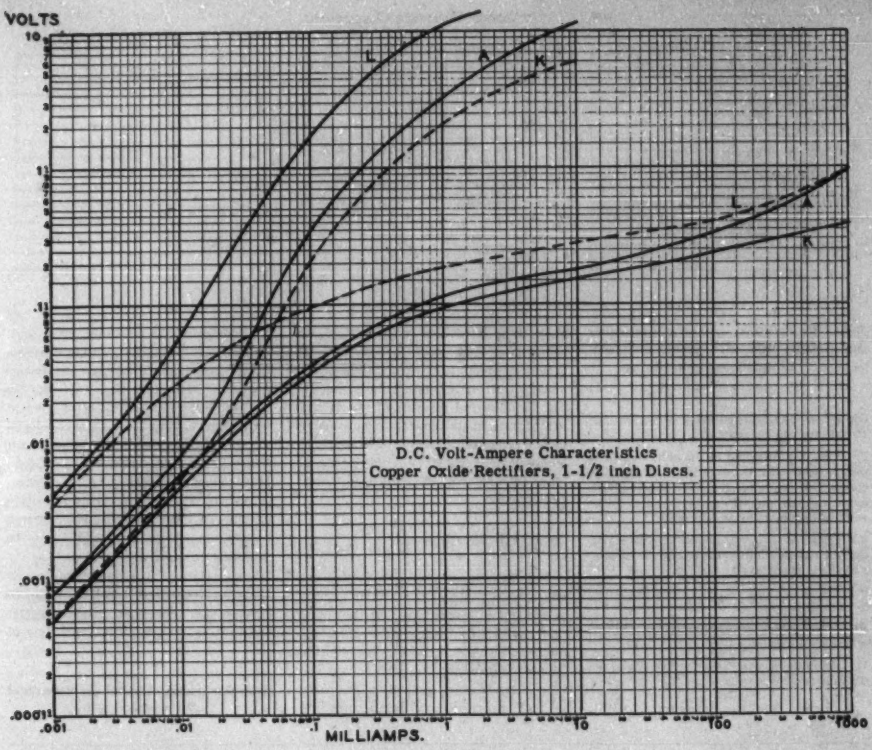


Figure 6. D-c volt ampere characteristics

which was capable of almost an infinite number of variations, became important. An enumeration of some of these details of production may be worth while and is given at this time.

1. *Temperature of Oxidation.* The first units were oxidized at a little over 900 degrees centigrade. It was soon found that the temperature of oxidation was important and that usually higher temperatures produced better results so that the temperature of oxidation was increased to 1,000 degrees centigrade, and later to the highest temperature that could be used without deforming the blanks by softening of the copper. Since then it has been found that some coppers give the best results at the highest possible temperature while other coppers give better results at somewhat lower temperatures of oxidation.

2. *Duration of Oxidation.* Obviously from a practical standpoint this should be as short as possible, and it was found practical to reduce it from the one hour that was used in the first units to 13 minutes and sometimes considerably less.

3. *Rate of Cooling.* This is very important in the control of the characteristics of the rectifier and will be discussed in greater detail.

4. *Purity of Copper.* Coppers from all available sources of supply were tried and while all were considered pure copper from a commercial standpoint, it was found that there were great differences among them when used in the production of copper oxide rectifiers. These differences were eventually found to be dependent on the presence of very small amounts of impurity in the copper.

5. *Surface Conditions.* In the early stages of the development, much emphasis was laid on the conditions of cleanness of the surface, and of its degree of roughness. A clean surface is still considered absolutely essential. The mechanical condition of the surface as well as the hardness of the copper are not now considered of great importance.

6. *Thickness of Copper.* The first units were made with copper that was 1/8 inch thick. It was found that when the oxidation takes place on one side only, the copper buckles due to unequal thermal contraction of the copper and the cuprous oxide. At first it was thought that this was important because it was believed that the cuprous oxide would crack when the units were straightened out under pressure. Later, however, it was found that this amount of bending of the units was not important so that it became possible then to reduce the thickness of copper from 1/8 inch to 0.05 inch, and later to 0.04 inch in the large units, and even to thinner sections than that for small units.

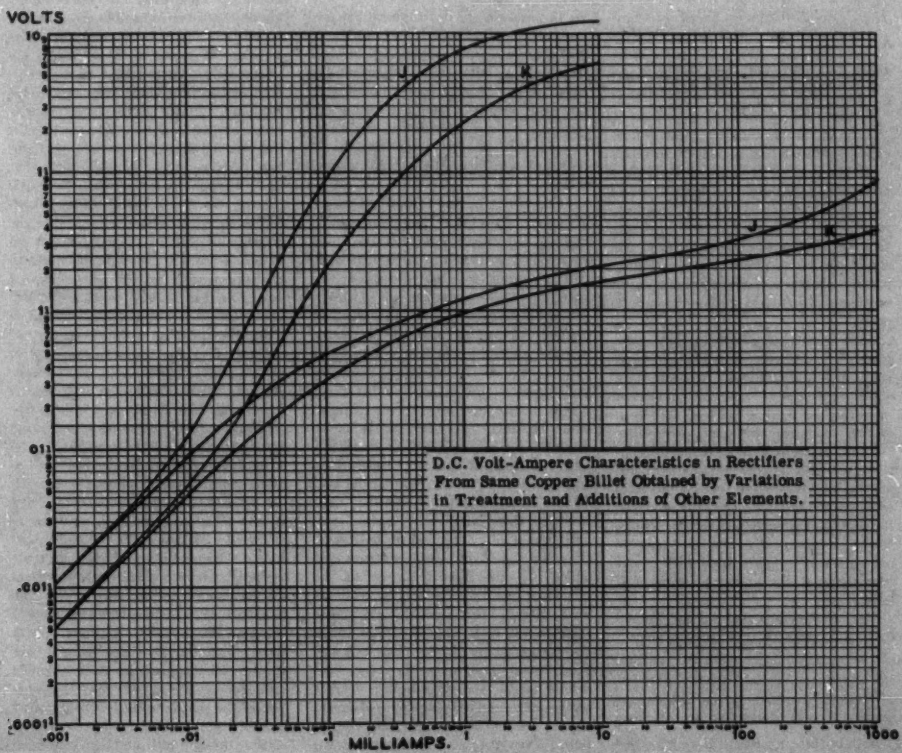
7. *Methods of Removing Black Oxide.* On account of the high resistance of the

cupric oxide layer which forms during the cooling of the disk from the oxidizing temperature, it became necessary to remove this layer. This was first done by grinding, later by sandblasting and by abrading with emery cloth, and finally by chemical solution. The first chemicals used for the solution of the cupric oxide were sodium or potassium cyanide. This was later changed to the use of mineral acids, which is the present practice.

8. *Methods of Making Contact to the Free Surface of the Cuprous Oxide.* This was considered a very important detail, because the forward resistance of the unit depends on the use of the entire cuprous oxide surface for the conduction of the useful current. It was, therefore, necessary to provide a contact that would reach into the depressions of the mat surface of the cuprous oxide, resulting after the removal of the black oxide. This contact was first made by applying a deformable metal such as lead. It was found that in operation the lead reacted with the cuprous oxide and reduced a part of the compound to copper, which made poor contact under these circumstances and increased the forward resistance. This condition was improved by applying a thin coat of tin to the lead, thereby reducing the reactivity of the material with the cuprous oxide. Later a more continuous contact was made by rubbing carbon or graphite into the surface of the cuprous oxide before applying the front contact. Still later this continuous contact was produced by painting the front surface with a suspension of carbon or graphite. Other methods of making contact have since been developed, such as reducing a portion of the cuprous oxide to copper and then electroplating over the copper with nickel. This produces a very satisfactory contact that is independent of atmospheric moisture, and which has very satisfactory low resistance. Another type of contact that is used on small rectifiers is made by allowing evaporated gold to condense on the cuprous oxide.

9. In the contacts that depend on a deformable metal for continuity, it is necessary to apply pressure. This is done in the disk assemblies by means of pressure applied by the bolt which holds the assembly together. Rather surprisingly it was found that pressures up to 1, 2, or even 3 thousand pounds per square inch can be applied to the cuprous oxide without injury, provided only that it is fairly uniformly distributed. This pressure has to be maintained during the life of the rectifier, and on account of variations in the length of the bolt and in the thickness of the disks due to temperature changes, it became necessary to apply the pressure through springs or spring washers that could take up such irregularities.

Figure 7. D-c volt ampere characteristics of $1^1/_2$ inch disks



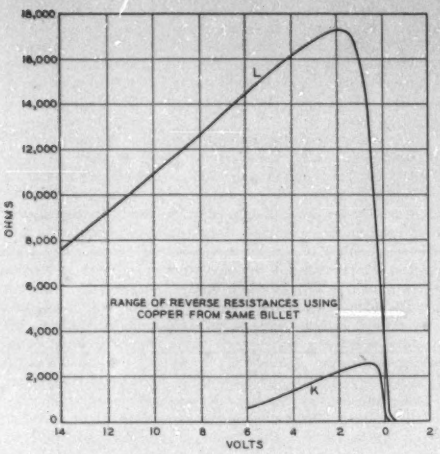


Figure 8. Range of reverse resistance using same copper

10. *Check of Different Kinds of Impurities in the Copper.* This became a very difficult problem which has been under study since 1921, and is still only partially solved. More will be said about that in later paragraphs.

11. At the beginning a great deal of experimentation had to be done to determine the appropriate operating voltages per disk and appropriate current densities. The effect of voltage, current, temperature, pressure, and other conditions on the life of the rectifier and the rate at which they would change with time became problems that have been under continous observation during the last 25 years.

After a few years of development some phases of rectifier production had been fairly well standardized. Washers were almost exclusively used for convenient mounting on bolts. Thirteen to fifteen minute oxidation was used universally. Carbon and lead were used for the contacts. A Chile copper prepared for rectifier production by special rolling processes was used universally because it produced the most consistently satisfactory results. Fifty-mil copper, with the least possible percentage of impurity and with a surface prepared suitable for polishing, was used.

The requirements that we had learned to consider essential were the following:

1. The lowest possible forward resistance and the highest possible reverse resistance.

2. Permanence, both in the forward and in the reverse resistance characteristics.

Both of these conditions have improved continuously as the development has progressed, and it would be interesting to plot a curve of each characteristic against time during which the development has taken place. Such a curve, however, is not easily drawn because the methods of testing have also changed continuously, and it is difficult now to correlate the recent test results with the early test results.

In the search for low forward resistance it was soon found that the rate of cooling had an important effect. If the disks were cooled in a blast of air instead of in still air, the forward resistance was lowered. The success of the air blast suggested the investigation of still more rapid methods of cooling, and the next step was the application of a very fine water spray. It was still considered necessary not to cool too rapidly because that might result in the cracking of the cuprous oxide crystals. The fine water spray produced further improvement, and it was finally decided to quench the disks in a water bath, and this resulted in still further improvement. It was then realized that there might be a possible optimum temperature from which to quench the disks. Another furnace was provided which has since become known as the annealing furnace, in which the rectifiers were placed after oxidation to bring them to definite temperatures from which the quenching could take place. It has been found that quenching for best results has to take place from a definite temperature, and that the length of anneal at this temperature is also very important. Even the rate of cooling between the oxidizing furnace and the annealing furnace is important. Quenching has the effect of reducing the forward resistance by a very large amount, and without having a corresponding effect on the reverse resistance so that the rectifying ratio and the quality of the rectifier for most applications is improved. The quenching has therefore become standard practice, except for very high resistance rectifiers for special applications, Figures 4 and 5.

Another condition that became exceedingly important as time went on was the presence of different impurities in the copper. Many suggestions have been made to explain the fact that Chile copper was more satisfactory for the production of rectifiers than any of the domestic coppers. It is not possible here to go into a discussion of all these suggestions, or to describe all the experiments that have been made in an attempt to determine what were the beneficial and what were the injurious impurities. It is definitely known that some impurities are valuable; for instance, it has been proved on many occasions that copper that contains approximately 0.03 per cent of oxygen is more satisfactory for rectifier production than are coppers with a greater or a lesser oxygen content. At one time it was believed that silver in the copper was very injurious, and that the reason that Chile copper is better than domestic coppers was that it contained less silver. At one time it was also believed that the presence of lead in the copper was an advantage, and an attempt was made to get copper with certain percentages of lead. Neither of these conclusions have been verified in the long run, and it is probably true that a small amount of either lead or silver is an advantage in most types of rectifiers, but that a larger amount, which may not be more than a few thousandths of 1 per cent, is definitely injurious. Thousands of experiments over many years have resulted in the acceptance of the following generalizations. Small amounts of metallic impurities increase the resistance of the rectifier in both directions, and may result in an increased stability of the reverse resistance. Nonmetallic impurities decrease the resistance in both directions, and result in a decreased stability of the high resistance, but also in a decreased amount of aging in the low resistance. Both types of materials have an effect not only on the resistance of the cuprous oxide itself, but on the resistance of the junction. The ideal toward which many have striven is to get as pure copper as possible to begin with, and then to add the

impurities that are necessary to get the characteristics required.

This has not been entirely successful, but in production at the Union Switch and Signal Company advantage is taken of such a program in the following manner. To increase the reverse current stability and the reverse resistance under operating conditions, a very small amount of iron is plated on the surface of the blank before it is oxidized. To decrease the forward resistance, which is largely located in the body of the cuprous oxide, the oxidation is allowed to take place in two stages: First, in a furnace is which the atmosphere is pure air, and second in one in which the atmosphere is pure air with a very minute addition of chlorine. This gives us a great amount of control of the characteristics of the final units in that we can vary the amount of iron, and we can vary the percentage of chlorine and also the length of oxidation in each of the two furnaces.

Another method of control that enables us to vary the characteristics is to combine with the use of these addition agents a variation in the temperature of oxidation and in the rate of cooling. Thus the cooling can vary from a slow cooling in still air to cooling in an air blast or to cooling in a water quench with all possible gradations between any two of these three conditions.

At this point I wish to show a set of curves of rectifier characteristics to indicate the variations that are possible, and the amount of control that is available in copper oxide rectifier production.

Figure 6 shows d-c volt ampere characteristics of three types of rectifiers; one high resistance, one low resistance, and one intermediate. The intermediate type is the kind of rectifier that is commonly in use for power applications such as battery charging, other electrolytic work, and the operation of small motors. The high resistance unit would be especially suitable for use in very high-voltage rectifiers where the current requirement is low. The low-resistance rectifier, on the other hand, might find uses where the output voltage requirement is low, say two or three volts, and the current requirement is comparatively high. It will be seen that the difference between the high-voltage rectifier and the low-voltage rectifier is a factor of three or more in resistance both in the high resistance direction and in the low-resistance direction.

Figure 7 shows two similar characteristics representing rectifiers made from the same kind of copper with the difference that the high-resistance unit is made with the addition of iron but no chlorine, and the low-resistance unit is made with the addition of chlorine and no iron. Both are water quenched. It will be seen that the variation in characteristics is quite large, and any intermediate value can be obtained by varying the amount of chlorine or iron.

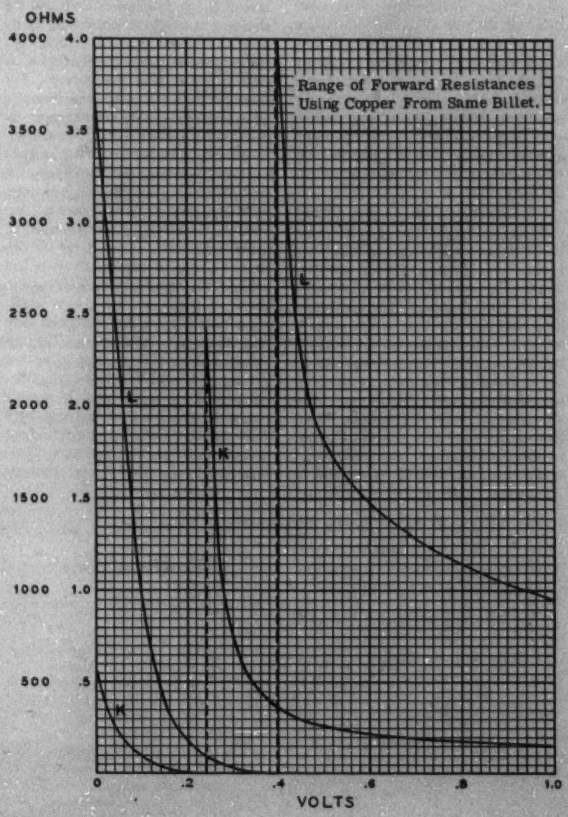
Figures 8 and 9 represent reverse and forward resistance voltage characteristics for disks made from the same copper, both made with chlorine and no iron; the high resistance unit being cooled in air and the low-resistance unit being water quenched. Again it is seen that control over a wide range of characteristics is available.

Methods of Testing

Another interesting chapter in the development would describe the methods used in testing the rectifiers to determine their quality and their usefulness in various applications. The correlation of test results with service behavior has been a very serious problem. In the early stages of the development, all tests were made at two volts, and all rectifiers were rated on the basis of forward resistance at two volts, and the reverse resistance at two volts, or in terms of what was known then as the rectification ratio, which is the ratio between these two resistances.

After some time it was realized that the reading of forward resistance at two volts does not tell the whole story, because the forward voltage in service is usually of the order of 0.5 instead of 2, and the reverse voltage in service is often of the order of

Figure 9. Range of forward resistance using same copper



six volts instead of two. The testing was then changed to the use of 0.5 volt and later 0.5 ampere in the forward direction and six volts in the reverse direction. The tests were still made by taking readings as quickly as possible on the test instruments. Later the reverse voltage used in the tests was raised to 10 volts. After many years it was realized that these quick tests did not correlate with the service performance of the disks. Sometimes a rectifier that seemed very unsatisfactory and that would be thrown out on the basis of quick tests was found to be entirely satisfactory in a service test. The problem then was to find a quick test that would correlate better with service performance, and after a great deal of search and much experimentation it was found that a quick test with the units at 70 degrees centigrade correlated very much better with service performance than did the test at room temperature. Even this was not entirely satisfactory, and is now supplemented by a stability test which consists in mounting sample disks in a stack and applying six volts d-c per disk for 24 hours. This test gives very good correlation with service performance, and can be used as a definite indication of the quality of copper oxide rectifier disks.

Life tests are obviously a very difficult problem because the rectifier lasts so long that a life test cannot be used as a guide in production. It has been found, however, that there is a definite relation between the life test performance at higher temperatures and the life test performance in service. This correlation is not absolute, but is good enough to give valuable information in a few weeks as to the performance that can be expected by a rectifier in service for many years. The reason that life tests are so difficult is that the rectifier itself has a very long life, in power applications, at least 20 years, and in other special applications probably at least 50 years.

Growth of Production

During the 25 years under consideration the copper oxide rectifier has enjoyed a steady increase in the number of applications and the quantity produced and used. The rectifier has a very wide range of applications both from the standpoint of types of service and from the standpoint of size and capacity. It has been used in applications from radio detection and instrument rectifiers to modulation and demodulation circuits in radio, and in carrier telephony, and to a wide variety of power applications. It has found many auxiliary uses which are based on the voltage or current characteristics of the units, such as the absorption of energy in magnetic coils and the snubbing of relays, the control of pick-up time and release time of relays, click reducers in telephones, polarization of circuits and of neutral apparatus. For these purposes the units have varied in capacity from a few microamperes, in disks that are only 0.03 inch in diameter, to currents of 25,000 to 100,000 amperes in plating rectifiers, and from microvolts to 1,500,000 volts in rectifiers used for radio detection or instruments at one extreme to voltages for experiments in atomic physics at the other extreme. The sizes of the units vary from vest-pocket editions that contain only a few milligrams of copper to units that weigh many tons. The power capacities vary from microwatts to kilowatts. The consumption of rectifiers during the last few years may be illustrated by saying that the amount of copper used in rectifier production is estimated to be between 1,500 and 2,000 tons per year. Without special provision for ventilation 2,000 tons would correspond to approximately 500,000 kilowatts of capacity per year. This capacity is very much increased by the fact that many of the units are provided with forced ventilation, so it can be assumed this estimate is conservative.

This very rapid review of the history of the copper oxide rectifier from its beginnings to the present time shows how it has grown from a single and very inefficient laboratory unit to an efficient rectifier extensively used in all phases of the electrical industry, and corresponding to that use, how it has grown in production to a sizable industry of its own. The development of such a unit is an exceedingly slow process for many reasons, and especially because it takes so long to determine whether a new type is or is not an improvement. Many research men and engineers in this country and abroad have been engaged in the development. All are agreed that there is still much to learn and, with the improved tools provided by the extensive research in semiconductors and the consequent better understanding of the behavior of materials in the solid state, the prospect is even more interesting than before, both from the practical and from the theoretical standpoint. It has been a pleasure to review and to tell the story, and those of us who have been active in the development appreciate very much the interest shown in it by the metallic rectifier subcommittee and by the AIEE.

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No Discussion

A Bridge Method for Determining the Motional Characteristics of Permanent-Magnet Movable-Coil Instrument Mechanisms

R. W. GILBERT
ASSOCIATE AIEE

Synopsis: The instrument mechanism is expressed in terms of an equivalent electric network, and the apparent electrical parameters determined by a-c bridge methods. The extracted parameters are: the apparent series resistance, the apparent damping resistance, the undamped period, and the periodicity at which the phase angle of reactance to the shunt resistance is −45 degrees. From these the periods and damping coefficients can be calculated. By including the d-c deflection sensitivity of the instrument the moment of inertia and the restoring torque gradient also can be calculated.

IN the design of d-c instruments motional characteristics such as period and damping sometimes must be accurately controlled. In production it may be desired to measure these factors conveniently for specification compliance. An example is the standard "VU" meter wherein the nature of the instrument's response is intimately associated with sound monitoring technique, and must be specified and controlled in production. In a completed and highly damped mechanism measurement by direct observation is not sufficiently accurate for inspection purposes, and examination formerly required at least partial disassembly.

During the course of some bridge measurements on a sound transducer in various acoustic surroundings by established methods, it became apparent that a similar technique could be applied to the analysis of instruments. The time parameters in this case are much longer but the mechanism is similar. As the method and theory developed, it further became apparent that more information than first anticipated could be extracted, specifically the mechanical parameters of torque and inertia.

The method here described pictures the instrument as a simple damped reactive electrical network, and measures its apparent electrical parameters by means of an a-c bridge. From the values thus assigned to the network the actual time and mechanical parameters are calculated.

In a companion paper[1] W. N. Goodwin, Jr., has developed the mathematical relationship between the mechanism as a mechanical system and its apparent electrical parameters. He therein also proves that the measurement is valid regardless of the nature of the interposed resistance network, if linear. These therefore are assumed evident, and the present paper is limited to the method of measurement, supported by theoretical description only as required in the course of explanation.

Terminology

The following terms are used in the discussion:

L,L' = apparent inductance, equivalent instrument network, henrys and abhenrys, respectively
C,C' = apparent capacitance, equivalent instrument network, farads and abfarads, respectively
R_f,R_f' = apparent internal damping resistance, equivalent instrument network, ohms and abohms, respectively
R_e = apparent series resistance, ohms
R_0 = critical damping resistance, total, ohms
T_p = periodicity, a-c bridge source, seconds per cycle
T_a = amplitude period, instrument, seconds per cycle
T = damped period, instrument, seconds per cycle
T_0 = undamped period, instrument, seconds per cycle
T_1 = periodicity, for −45° phase angle of R_f and C_1, seconds per cycle
C_1 = effective capacitance, instrument, for −45-degree phase angle against R_f
P = bridge ratio, numeric
K = moment of inertia, instrument, gram centimeters squared
S = control torque, instrument, dyne centimeters per radian
B = air gap flux density, gausses
A = effective movable coil area, square centimeters
N = number of turns on movable coil
q = attenuation ratio of resistance network, numeric (unity for unshunted instrument)
I' = current sensitivity constant, instrument, abamperes per radian
η = specific damping coefficient numeric

Equivalent Network

It is well recognized that resonant mechanical and electric systems are rigorous analogues of each other, and are expressible in similar, and even identical, terms. Furthermore, the permanent-magnet movable-coil mechanism such as a d-c instrument includes electromechanical coupling and appears as an electrical resonant network at the terminals. In this case the analogue becomes also an equivalent and may be measured as such. This equivalence has been of particular value in the analysis and expression of

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R. W. Gilbert is with the electronic engineering division, Weston Electrical Instrument Corporation, Newark, N. J.

The calculations in considerable part were derived from the basic work of W. N. Goodwin, Jr. (F '13), particularly in relating the electrical to the mechanical parameters. Also, the assistance of F. V. Masson who designed and built the bridge of Figure 3 is appreciated.

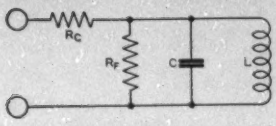


Figure 1. Equivalent network of movable coil instrument

electroacoustic transducers, to most of which the d-c mechanism is similar, but which have parameters of a considerably different order of magnitude.

The permanent-magnet movable-coil mechanism may be expressed by the equivalent circuit and the elements of Figure 1. It is essentially a parallel resonant inductance L and capacitance C shunted by a loss resistance R_f, and connected through a series resistance R_c. These particular elements may not have exact physical counterparts in the mechanism, but any mechanism is expressible in these terms so that the mechanism and its equivalent circuit are ideally indistinguishable at their terminals.

In terms of electromechanical equivalence the capacitance C represents the moment of inertia, the inductance L the spring compliance (reciprocal torque), and the resistance R_f the loss both frictional and electrical. The series resistance R_c is the terminal d-c resistance, which in unshunted instruments is the movable coil resistance. Note in contrast that general electromechanical analogues associate capacitance with compliance and inductance with inertia, whereas the reverse is true here because of the inversion produced by electromagnetic coupling.

The instantaneous position of the instrument pointer is related directly to the force level in the spring compliance, which is equivalent to the current in inductance L. This relationship is useful in determining the nature of the pointer response to complex or nonrecurrent input wave forms or transients. In cases difficult to calculate a "mockup" of the network using electric components will make an oscillographic determination possible. In such an experimental determination it may be necessary to "scale-up" the order of frequency, which is permissible if the ratio $(L/C)^{1/2}$ is maintained in relation to the network resistances.

It is broadly apparent that when a mechanism is expressed in terms of its equivalent network, powerful tools for its analysis exist in available methods for network treatment, both theoretical and experimental. No comparable treatment appears to exist in direct mechanical analysis. This is strongly evident in the present trend toward electrical analogue computation of mechanical problems as a matter of practical convenience, and conventional methods for specific problems are too detailed and extensive for presentation here.

Electromechanical Flux Linkage

In the d-c mechanism the electric input is coupled to the mechanical motion of the system through the force-current reaction of the movable coil in the magnetic field. Coil current causes a mechanical force, and in reciprocity coil motion causes a potential reaction, both being dependent upon the subtended magnetic flux, or "flux linkage." In the usual case of a coil pivotally suspended within a uniform radial flux field the flux linkage may be expressed as the product of flux density B, the coil area A, and the number of coil turns N; the so-called "BAN factor."

In the equivalent network the flux linkage would appear as an ideal transformer of turns ratio BAN connected between the actual mechanical sources of L and C, and the resistances. The corresponding impedance ratio is $(BAN)^2$. This transformer cannot alter the resonant frequency, so the LC product remains constant, but it will alter the impedance so the L/C ratio is susceptible. Therefore, if it is desired to determine the actual mechanical parameters of torque and moment of inertia from the measured values of L and C, it is necessary to include the BAN factor.

While the separate constituents of the BAN factor are not determinable by external measurement, the BAN factor proper is related to the movable coil current–deflection sensitivity of the mechanism. But the instrument may include a shunting network so that the movable coil current sensitivity is not measurable. It is then necessary to include the attenuation factor of the intervening network q which is the ratio of the movable coil current to the applied terminal current. q is then unity in the case of unshunted instruments and smaller than unity for shunted instruments. The flux linkage factor BAN then becomes an apparent flux linkage factor $(qBAN)$ which cannot be further dissociated without knowledge of the internal construction.

From the basic relationship existing between consistent units, a unit current flowing through a conductor of unit length in a magnetic field of unit density will develop a unit of mechanical force. By simple geometric derivation it is then apparent that a movable coil suspended within a uniform radial magnetic field will develop a torque equal to the product of the coil current and the BAN factor. When equipped with spring restoring force and a possible interposed resistance network

$$S = I'qBAN \tag{1}$$

wherein S is the torque-deflection (rigidity) constant of the spring and I' is the current-deflection constant of the instrument, with the deflection unit expressed in the same term for both, usually per radian.

The $qBAN$ factor has no influence upon the resonant frequency, so

$$L'C' = \frac{K}{S} \tag{2}$$

But the impedance dimensions $(L'/C')^{1/2}$ and $(KS)^{1/2}$ are proportional to the $qBAN$ factor squared, so

$$\frac{L'}{C'} = KS(qBAN)^4 \tag{3}$$

Solution of equations 1, 2, and 3 gives

$$qBAN = I'L' = (I'C')^{-1/4} \tag{4}$$

The $qBAN$ factor is thus calculable from equation 4 by knowing L' or C', and I'.

Damping

The LC circuit of Figure 1 is damped by the loss-resistance R_f. The circuit will be critically damped when

$$R_f(\text{critical}) = {}^1/_2(L/C)^{1/2} \equiv R_0 \tag{5}$$

and for lower values of R_f the system will be aperiodic.

In instrument practice wherein highly damped systems are usual a useful coefficient is the "specific damping coefficient" n which is defined as

$$n \equiv \frac{R_0}{R_f} \tag{6}$$

n is unity for the critical damped condition and zero for the undamped condition. Other useful coefficients of damping are the log decrement and its related expression the "overshoot ratio" k, the latter defined as the amplitude ratio of successive half cycles of free oscillation.

$$\text{log decrement} \equiv \log_e k = \frac{\pi n}{(1-n^2)^{1/2}} \tag{7}$$

Of the time parameters three are of usual interest; the undamped period, the damped period, and the amplitude period, which are defined as follows:

Undamped Period (T_0)—The natural period that would result if the system were loss-free. In networks this is sometimes called

the "phase period," because it is the period of forced oscillation, regardless of damping, at which the reactance parameters cancel and the network appears entirely resistive.

Damped Period (T)—The time interval existing between successive full cycles of free damped oscillation.

Amplitude Period (T_a)—The periodicity of forced oscillation at which the ratio of pointer amplitude to the applied current is a maximum.

Their values in terms of the circuit elements of Figure 1 and the equivalent mechanical expressions are as follows:

Period	Symbol	Electrical Parallel Circuit Expression	Mechanical Expression	
Undamped	T_0	$2\pi\,(LC)^{1/2}$	$2\pi\left[\frac{K}{S}\right]^{1/2}$	(8)
Damped	T	$\dfrac{2\pi(LC)^{1/2}}{\left[1-\frac{L}{4CR_f^2}\right]^{1/2}}$	$\dfrac{2\pi\left[\frac{K}{S}\right]^{1/2}}{\left[1-\frac{(qBAN)^2}{4KSR_f'^2}\right]^{1/2}}$	(9)
Amplitude	T_a	$\dfrac{2\pi(LC)^{1/2}}{\left[1-\frac{L}{2CR_f^2}\right]^{1/2}}$	$\dfrac{2\pi\left[\frac{K}{S}\right]^{1/2}}{\left[1-\frac{(qBAN)^2}{2KSR_f'^2}\right]^{1/2}}$	(10)

The ratio of the damped period T and the amplitude period T_a, to the undamped period T_0 relate to the specific damping coefficient n, as

$$\frac{T_0}{T}=(1-n^2)^{1/2} \tag{11}$$

and

$$\frac{T_0}{T_a}=(1-2n^2)^{1/2} \tag{12}$$

which indicate the convenience of n as a practical expression of damping in relation to the time parameters.

From equation 11 it is apparent that T will become infinite when n becomes unity, and will be imaginary for values of n larger than unity. And from equation 12 the same will occur for T_a when n becomes equal to $2^{-1/2}$ and larger. When T_a is infinite or imaginary the largest deflection of the instrument will occur with direct current.

Note that the damped period and the amplitude period become equal to the undamped period T_0 when n becomes zero, and that at low values of n the separation is small.

Upon consideration it is apparent that a damped system has only one period of natural behavior, its damped period T, and that the other periods result from forced rather than free oscillation to produce certain conditions. But in practical calculations it is considered more direct to express the mechanism in terms of its undamped period T_0 as the time parameter, with n as the damping parameter.

Mechanical Parameters

The mechanical parameters of spring rigidity S and moment of inertia K directly relate to the inductance and capacitance of the network, provided the units are consistent and the interposition of the $qBAN$ factor is included. Thus, by conversion of the network parameters L and C into absolute units (L' and C') and knowing $qBAN$ the mechanical parameters can be determined. Or, alternatively, the current sensitivity I' rather than $qBAN$ may be used as a more direct process.

From equations 1 and 4 by substitution

$$S=I'^2L' \tag{13}$$

which by substitution in equation 2 gives

$$K=I'^2L'^2C' \tag{14}$$

Bridge Method of Measurement

The measurement bridge in functional form is shown in Figure 2. It contains the instrument mechanism as one arm and has a balancing arm comprising the series and parallel resistances, PR_c and PR_f, and a capacitance C_1+P in shunt to PR_f. The bridge ratio P may be large to accommodate the rather large effective values of reactance exhibited by the instrument. The bridge source is alternatively direct current and low frequency alternating current of adjustable known periodicity. The galvanometer serves for a-c balance as well as d-c balance because of the low a-c frequency ordinarily required.

While such a bridge may be manipulated in a number of ways to determine the equivalent network, it is necessary to employ at least two frequencies to extract L and C independently. The present method for convenient calculation determines the undamped period T_0 and the periodicity T_1 at which the apparent reactance is capacitive and has a phase angle of -45 degrees against the loss resistance R_f. At this point the phase angle tangent is unity and the apparent reactance is equal to the loss resistance R_f. This likewise proves to be the point of best bridge resolution with regard to reactance.

The bridge is operated in three steps; determining R_c, T_0, and R_f, and finally T_1, in order. These operations in detail are as follows:

1. With PR_f at zero and the d-c bridge source connected, balance PR_c to establish the series resistance R_c. The PR_c setting thus determined is left unaltered during the following procedures.

2. With C_1/P at zero and the variable periodicity a-c source connected, balance the source periodicity T_s, and PR_f. The instrument will oscillate and must be kept free of the pointer stops and preferably at a small amplitude. The balance point is coincidental to specific values of T_s and PR_f, similarly to a conventional a-c bridge wherein magnitude and phase angle must be independently adjusted. PR_f then represents the loss resistance R_f, and T_s is the undamped or zero phase angle period (T_0) of the instrument. Leave the PR_f setting intact.

3. Decrease T_s (increasing frequency) and add C_1+P to rebalance. Each decrease of T_s can be rebalanced by an increase of C_1+P, and the ability of C_1+P to rebalance the bridge is continuous. Find the balance point where the impedance of C_1+P is equal to the resistance PR_f at periodicity T_s. At this point the tangent phase angle is unity, and T_s represents T_1.

This third operation requires a running

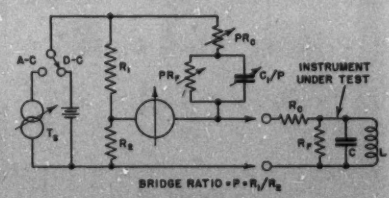


Figure 2. Functional circuit of a-c d-c bridge for measuring instrument parameters

calculation during balancing because both capacitance and periodicity are changing. However, it may be simplified by use of a slide rule to express the relationship at the proper balance point as

$$\frac{T_1}{C_1/P}=2\pi PR_f \tag{15}$$

by setting the slide to $2\pi PR_f$. Balancing may then proceed to the point where the ratio of T_s to $C_1 \div P$ appears on the slide rule.

From T_0, T_1, and R_f as measured, the network parameters of interest may be calculated from

$$T=T_0\left[1-\frac{\left[\frac{T_0}{T_1}-\frac{T_1}{T_0}\right]^2}{4}\right]^{-1/2} \tag{16}$$

$$T_a=T_0\left[1-\frac{\left[\frac{T_0}{T_1}-\frac{T_1}{T_0}\right]^2}{2}\right]^{-1/2} \tag{17}$$

$$C=\frac{T_1T_0^2}{2\pi R_f(T_0^2-T_1^2)} \tag{18}$$

$$L=\frac{R_f(T_0^2-T_1^2)}{2\pi T_1} \tag{19}$$

Figure 3. Complete bridge for instrument analysis, front and rear views

which are modifications of well known resonant circuit expressions.

From equations 11 and 16

$$n=\frac{\frac{T_0}{T_1}-\frac{T_1}{T_0}}{2} \tag{20}$$

and from equations 6 and 20

$$R_0=nR_f=R_f\frac{\frac{T_0}{T_1}-\frac{T_1}{T_0}}{2} \tag{21}$$

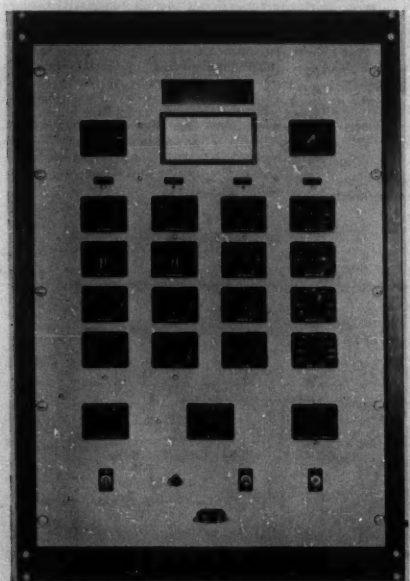
At a-c balance distortion products in the bridge source or generated within the instrument mechanism will appear in the galvanometer as harmonics. It is therefore desirable to use a bridge source of fairly pure wave form. Distortion produced within the mechanism may result from nonuniform flux distribution, air or pivot friction, or any interference with free motion such as contacting the pointer stops. Most frictional factors are nonlinear and develop nonlinearity of loss resistance R_f. Nonuniformity of flux distribution in effect causes a modulation of the BAN factor, and is equivalent to a changing L/C ratio, and a changing C_1 for balance.

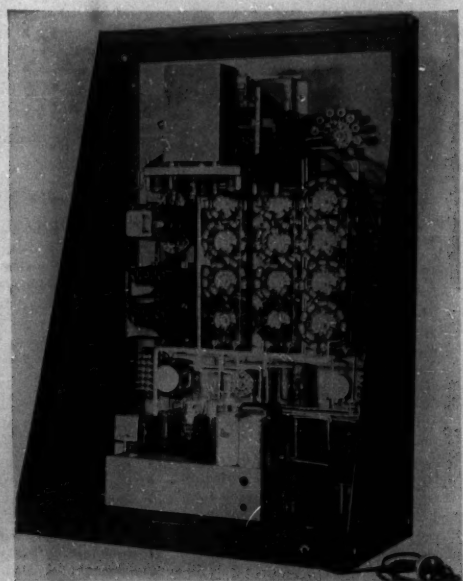
The foregoing assumes that the capacitors comprising $C \div P$ are free of losses. But at the low frequencies used they may develop an unexpected phase angle as evidenced by an inability to obtain a clean bridge balance in the last operation. In such cases the balance may be improved by slightly increasing PR_f to compensate, but retaining the value of PR_f obtained in the second operation for purpose of calculation.

In some cases it is necessary to deflect the instrument to a desired point on the scale or away from interference by the pointer stops. This requires injection of an additional d-c deflection current during the a-c balancing operations, without shunting the bridge arms. In the bridge of Figure 2 this deflection current may be injected into the galvanometer connection, which is at an a-c nul at balance, with a blocking capacitor to isolate the galvanometer.

Figure 3 shows the constructed form of the Figure 2 bridge as used in the laboratory. By adjustment of the bridge ratio over a considerable range it is possible to measure instruments of all usual sensitivities. It performs all the necessary measurements except the deflection sensitivity I' which is measured directly by observation.

The oscillator used for a-c source is critical regarding purity of wave form and was specially designed for the purpose. It is a modified Scott R-C oscillator with the periodicity adjustable by a dual dec-





ade resistance, which calibrates directly in periodicity rather than frequency. The total harmonic content below the 7th order is less than 0.25 per cent.

Accuracy

The accuracy of the method is limited theoretically only by the resolution of the bridge, which is largely optional. But the practical limit is determined by distortion products obscuring the galvanometer nul. The major distortion in practice arises from magnetic nonuniformity present in some degree in all instruments. Distortion from this source can be reduced by reducing the pointer amplitude, and if necessary increasing the galvanometer sensitivity correspondingly.

So accuracy is primarily a matter of desire and design, with the bridge of Figure 3 a satisfactory mean for most purposes.

The over-all operating accuracy with careful manipulation is considered as 0.5 per cent of the measured values. In the case of highly damped instruments this is far superior to any method employing direct observation, and in any event is considerably more accurate than normally required.

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No Discussion

Three-Compartment Articulated Cars for Test on Rapid Transit Division, Chicago Transit Authority

H. A. OTIS
NONMEMBER AIEE

A FEW YEARS AGO, the Chicago Rapid Transit Company saw the possibility of building a vehicle for service on the elevated and through the subway, in the construction and equipping of which many of the parts could be used that had become standard for the Presidents' Conference Committee (PCC) street car. Each of these parts had a definite limitation in its capacity. At that time, the basic train operated on our system during mid-day and at night was two cars so it seemed desirable to include in one vehicle the equivalent passenger capacity. With this passenger capacity and the weight limitations of the equipment in mind a vehicle was designed.

Due to the fact that the vehicle as designed was entirely different from any on our property it was decided to obtain four cars to prove out in actual operation the design and equipment. In order to further the test, two cars were ordered from one car builder and two cars from another. Also, as far as possible, the parts and equipment as installed on the cars as built by one car builder were different from those as installed in the cars by the other builder.

The two cars of one builder have been received and are undergoing test in regular service, while the cars from the other builder are expected soon.

Body and Equipment

Each car has three articulated bodies mounted on four trucks. The length over anticlimbers is 88 feet $7^1/_2$ inches, the middle truck centers are 25 feet 10 inches and the outside truck centers 24 feet $4^3/_4$ inches. In order to hold the weight to a minimum the entire body is made of aluminum except the end underframes beyond the bolsters and the bolsters which are made of steel. The car, complete, weighs 93,000 pounds. The car has a seating capacity of 96, and a total capacity of 250, which is approximately the same as two of our older cars. This is within the weight limitations of the standard PCC motors to give the operating characteristics which we desired.

Dut to structural limitations on the system the width over the side sills is limited to 8 feet 8 inches. In order to obtain greater width at the top of the seat cushion the sides curve out from the top of the side sills to a width of 9 feet 4 inches at seat level height and then back into a width of 9 feet 0 inch at the eaves. Curving the sides out makes possible a 39-inch wide seat and a 30-inch aisle.

A motorman's cab is built in at the right-hand corner of each outside end of the car. This cab contains the power and brake controllers, an enclosed switchboard at the rear of the cab, a gang of operating switches and a side entrance door control switch.

At each articulated joint there is an open archway in each body which permits uninterrupted passage from one compartment to another. The space between bodies is closed by a canvas diaphragm fastened to both bodies. At the outside ends are train doors which permit passage between cars.

Built into each end of the car are two headlights, a curtain destination sign and four illuminated combination tail and classification lights having four colors in each.

The motorman's front cab window is equipped with an electric windshield wiper and inside window defroster both with speed control.

The side windows are raised and lowered by means of automotive type operators with the cranks located above the windows. All windows raise and their travel is limited to eight inches. All glass throughout the car is safety sheet except that in the front cab window which is safety plate.

The car lights are single fixtures of the controlled-beam type, one located over each seat. The lamps used are the 20 in series 30-volt 1-ampere short-circuiting type. The lights are remotely controlled and can be turned on or off on the whole train from any motorman's cab or a switchboard on the center compartment.

The seats are all 35 inches wide, set perpendicularly to the side of the car and upholstered in mohair. The seats are stationary and set so that one-half of the passengers ride backwards. The side wall of the car between window posts and from the bottom of the window to below the top of the seat cushion is cut back to about one inch thick. The edge of the seat cushion is set four inches from the side wall which in effect gives a seat cushion 39 inches wide.

Drawbars

The drawbars are M. C. B. (Master Car Builders) tight lock type and furnished by the Ohio Brass Company. Attached to the bottom of the head is an electric circuit coupler portion with 56 contacts. The drawbars can be uncoupled by pressing a button located either in an adjacent motorman's cab or under the car body end sill near the side of the car. The pressing of any of these buttons starts a progressive action that

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H. A. Otis is engineer of car equipment, Chicago Transit Authority, Chicago, Ill.

opens the electric jumper circuits by throwing a drum switch on each car, starts a hydraulic pump which forces fluid into a cylinder through which a moving piston pulls back the drawbar lock to complete the uncoupling.

Heating and Ventilation End Compartments

The traction motors accelerating and braking resistors are enclosed the same as on the street cars and the waste heat from them is used to help heat the car. Each compartment is equipped with a motor generator set to supply the power for the low-voltage circuits. The motor generator sets also have a two section blower mounted on each end of the shaft. Each one of these blowers draws 425 cubic feet per minute (cfm) of air out of the car body through a grill in one of the center door posts and 325 cfm of outside air through a grill and register located in the outside of the center door post directly across the car. These two volumes of air are mixed together in the blower and are forced into the resistor enclosure. Thus, the two blowers together exhaust 850 cfm of air out of the body, mix it with 650 cfm of outside air and force the 1,500 cfm of mixed air into the resistor enclosure. While passing over the enclosed resistors this air becomes heated and passes out through a duct at the bottom of the enclosure. In this duct is a motor-driven damper which either exhausts this air to the atmosphere or into the car body.

Temperature thermostats are located in the duct containing the air exhausted from the car body. If the temperature of this air is below the setting of the thermostat it closes a circuit to cause the damper motor to move the damper which sends more hot air into the body. If the temperature of the return air is above the setting of the thermostat it closes a circuit to cause the damper motor to move the damper to exhaust more of the heated air. The exhaust air is conducted through ducts to the space between the compartments and there exhausted to the atmosphere. The heated air being directed to the interior of the body passes over electric heaters and a thermostat set at 100 degrees fahrenheit. If the temperature of the air going to the interior of the body is not up to 100 degrees the electric heaters are turned on to bring up the temperature. Air going to the interior enters four longitudinal ducts in the floor. Air from these ducts then enters the body through louvers on each side of each aisle seat pedestal and through grills in the side wall below the seat cushions. The circuit of the damper motor contains a time-delay relay which makes the motor circuit a pulsating circuit and causes the damper to move slowly toward either the open or closed position.

Above a flat section of ceiling at the articulated end a motor is located driving two blowers that draw in outside air through grills in the roof, pass the air over electric heaters and into longitudinal ducts in the ceiling, from which it is exhausted into the car. In the duct is a thermostat set at 50 degrees that controls the electric heaters to bring the temperature of this outside air up to 50 degrees before entering the car. In series with the motor of this blower, there is a rheostat connected to the dump damper under the car and so arranged that as more heated air is exhausted to the atmosphere, the speed of the overhead blower is increased by reducing the resistance. In the ventilating system under the car, a fixed amount of fresh air is taken into the system. When some of the heated air is exhausted to the atmosphere it carries with it a proportion of the outside air taken in. The overhead fresh-air system makes up for the fresh air in the underneath system that is being exhausted. Also, during hot weather when all of the hot air is being exhausted, the overhead system runs at its maximum speed and forces cool outside air in at the ceiling line where the hot air tends to collect.

With the accelerating and braking resistors enclosed it is necessary that the motor generator set be in operation at all times that the car is in operation. Advantage was taken of this fact to eliminate all heating and ventilation switches and control. Whenever the motor generator sets are in operation circuits are set up and the heating and ventilation are entirely automatic under the control of the thermostats.

Center Compartment

There are no accelerating and braking resistors on the center compartments so that the entire heating must be done by electric heaters. As on the end compartments, air is drawn out of the interior by a blower on the shaft of the motor generator set, is passed over the electric heaters in an enclosure, and back into the body through the seat pedestals and grills in the side walls. Thermostats located in the air stream control the electric heaters and also a motor that varies the resistance in series with the overhead fresh-air intake-blower motor. The amount of fresh air introduced at the ceiling depends upon the temperature of the interior air. The minimum speed of this intake blower is sufficient to furnish 10 cfm for each seated passenger.

Side Entrance Doors

On each side of each compartment there are two entrance and exit doors separated by a post. Each opening consists of two inwardly folding doors of the PCC type, that when open provides a 24-inch passage. These doors are electrically driven and may be remotely controlled in any desired combination from a number of different operating positions. Door operating positions are in each motorman's cab and between the side entrance doors in each center compartment. By the operation of zone switches located on the switchboard in each motorman's cab any desired combination of side doors may be controlled from any operating position.

In each motorman's cab there are signal lights which are lit when all side doors are closed and out when any one door in the train is open. Thus, when the signal lights in the operating cab are on, the motorman may start his train. Above each group of side doors, there is a light which is on when either door at that position is open. This light has a red lens to the front and rear to give an indication when the door is open and a clear lens on the bottom to light the platform near the door. The positive feed to the master controller in the operating cab is through a relay in the side door signal circuit, so that when any one side door in the train is open, the control-positive feed to the operating controller is open.

Trucks

The trucks are basically the same as under the PCC street cars except that due to the heavier king-pin loads, it was necessary to strengthen all parts. The principal changes were the increase in the diameter of the resiliant wheels from 25-to-28 inches in order to obtain more rubber, the use of a $5^1/_8$ inch wide wheel and a 5 inch instead of a 4 inch diameter axle. The railroad's standard third-rail current-collecting device is attached to the truck.

Propulsion Motors

The motors on these cars are standard PCC street-car motors mounted lengthwise of the trucks and connected to the axles through hypoid gearing. The motors are force ventilated with air furnished by a section of each blower on the motor-generator set shaft. These motors, as on the street cars, not only furnish the power

for propulsion but also through dynamic brake circuits also stop the car.

Motor Control

The motor-control equipment also is nearly the same as on the street cars, the only difference being those changes necessary to make the control multiple unit. The amount of current flowing through the motors in both propulsion and braking, and thus the rate of acceleration and braking, is under the control of a current-limit switch. On the street car the spring which controls the amount of current through the limit switch is connected to the operator's position by levers and rods. The operator can obtain an infinite number of settings of the spring and thus the rates of acceleration and deceleration. For multiple-unit operation three fixed rates were selected for both, and they are obtained by passing a fixed amount of current through three different coils on the limit switch which in reality stretches the limit-switch spring three different amounts. The rates of acceleration and deceleration which were selected correspond to approximately one, two and three miles per hour per second.

The master controller, located in each motorman's cab, contains a central coasting position with the acceleration positions to the right of the coasting position corresponding to the three rates of acceleration and with four positions of braking corresponding to the three rates of braking and an emergency rate to the left of the coasting position.

A motorman in the operation of this train, after noting the door-signal light is on places the controller handle in the position to give the desired rate of acceleration, namely one, two or three miles per hour per second. After the train has obtained the desired speed the controller handle is placed in the coast position. When it is desired to stop the train, the controller handle is placed in the position to give the desired rate of retardation and left in that position until the train stops. When the controller handle is placed in the coast position the braking circuits are set up so that retardation is obtained at once, when the controller handle is moved to the braking position. The motors act as the sole brake until the speed is reduced to just a few miles an hour, when the dynamic brake fades out due to the low-generated current of the motors. At the current setting of a relay called the lock-out relay, the dynamic-motor circuit is opened and the drum brake on the motor shaft is applied, which brings the train to the final stop.

When the controller handle is placed in a braking position and current flows through the dynamic-braking circuits the motors act as a brake and the lockout relay prevents the armature-shaft drum brake from being applied until the current through the motors drops below the setting of this relay. Should current fail to flow through the dynamic-brake circuit, the armature-shaft brake comes on automatically. This armature-shaft brake will also give decelerating rates of approximately one, two or three miles per hour per second depending on the amount of current through the application coil.

No Discussion

Attenuator Materials, Attenuators and Terminations for Microwaves

G. K. TEAL
NONMEMBER AIEE

M. D. RIGTERINK
NONMEMBER AIEE

C. J. FROSCH
NONMEMBER AIEE

THE INCIDENCE of the last war gave rise to demands for radar and communication systems requiring the development of a large variety of microwave devices. A considerable number of these employed "lossy" solids that we have called attenuator materials since they may differ considerably from those solids normally classed as resistor materials. Many engineers at the Bell Laboratories were engaged in various phases of the development of such circuit elements.[1] Although the present paper discusses the development primarily from the materials point of view, the related problem of design was one that could not be separated entirely from the materials aspects and, therefore, is touched upon too.

While it is not intended to present a review it may be of interest to mention other work in the field. The pioneering experiments in the field were done by Southworth, Bowen, King, and their coworkers.[2] More recently, valuable high frequency applications have been made of the deposited carbon resistor as developed in these laboratories by R. O. Grisdale and his coworkers[3] and of the evaporated metal film of the Polytechnic Institute of Brooklyn. Much work on attenuating devices was carried out in other research centers, particularly at the Radiation Laboratory.[4] The materials discussed in the following paragraphs differ from these and were developed primarily to meet the requirements of high power usage in radar systems. At the time the needs arose there were virtually no attenuator materials available for many new applications. The shortcomings of the few known materials fell into one or more of the following categories:

1. They were unable to handle the available power.
2. The methods of preparing them did not permit standardization to give dependable electrical characteristics.
3. They were not stable.
4. They were not suited to giving the desired physical configuration.
5. They did not permit the attainment of some special characteristic that was required for a specific job, for instance, constant attenuation with absorption of high power.

In Figure 1 are shown in simplified form two microwave systems to illustrate typical uses of attenuator materials. In the case of the measuring equipment shown in (*a*) a signal generator is sending radio frequency signals *A* of a few centimeters wave length into a metal wave guide. Normally the signal generator is protected from reaction from the remainder of the system by a pad *B* of 10 to 30-decibel attenuation inserted at this point to stabilize the oscillator against variations in frequency caused by changes in the load impedance. In order to adjust the output level of the signal by measurable amounts or to reduce it to a conveniently measurable level a variable attenuator *C* is provided in the circuit at the point shown. It is followed by the material or device whose characteristics are to be determined. Another variable attenuator *D* may be inserted between the material under study and the receiver *E* to permit adjustment of the output level. The receiver is a sensitive element, such as a thermistor or detector, which is actuated by the output signal.

The case of a radar system is illustrated in (*b*) of Figure 1. Since the output of this system should be as nearly the maxi-

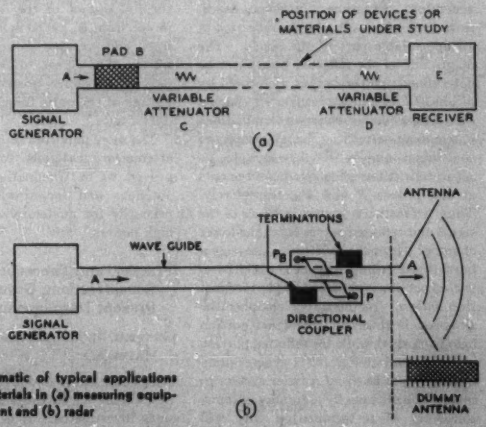

Figure 1. Schematic of typical applications of attenuator materials in (a) measuring equipment and (b) radar

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G. K. Teal, M. D. Rigterink, and C. J. Frosch are all with the Bell Telephone Laboratories, Inc., Murray Hill, N. J.

The authors acknowledge the work of their associates in other groups and the invaluable assistance of W. A. Tyrrell of the Holmdel Laboratory.

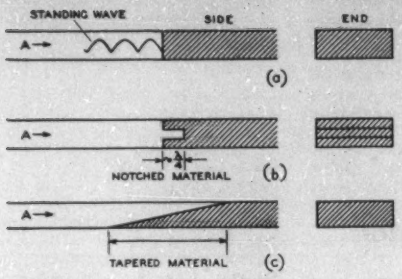


Figure 2. Methods of reducing reflections from attenuator structures in wave guide

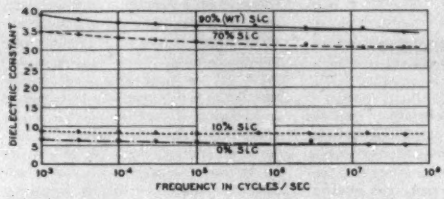


Figure 3. Dependence of the dielectric constant of silicon carbide plus porcelain attenuator materials on the semiconductor content and on frequency

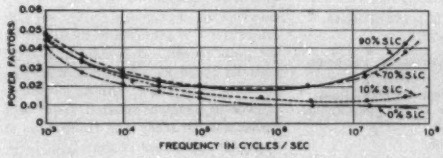


Figure 4. Dependence of the power factor of silicon carbide plus porcelain attenuator materials on the semiconductor content and on frequency

mum output of the magnetron generator as possible, the attenuation in the wave guide is reduced to a minimum and the antenna which radiates the beam into space is designed to give a minimum reflection of the signal back towards the signal generator. Sometimes, however, conditions may arise leading to the reflection of an appreciable part of the signal. The magnitude of the reflected power can be determined and the output monitored by means of a directional coupler.[4,5] Such a directional coupler employs terminations in a manner permitting the magnitudes of the signal and reflected waves to be separately determined by measurements at the probes P and P_B, respectively. Thus, for instance, known portions of the signal and reflected waves enter the lower chamber through apertures, the signal portion proceeding to probe P while the reflected portion is absorbed by the termination shown. In the upper chamber the situation is reversed, the signal portion being absorbed, while the reflected portion is determined at P_B. It is general practice during tune up of a radar system to replace the antenna by a dummy antenna consisting of a termination that will effectively absorb all the energy normally radiated. This is necessary to reduce interference between systems and to prevent the detection by the enemy of radiated power during tune up. To permit tune up the dummy antenna must absorb the full output of the transmitter peak power from 50 kw to more than a megawatt and average powers of 50–1,200 watts. The great difference between peak and average powers is due to the pulsing of the radio frequency with short duty cycles.

The more important applications of the attenuator materials to be described proved to be terminations for dummy antennas and for directional couplers, primarily for moderately high and very high powers.

Practical and Theoretical Considerations Governing the Present Development

General Requirements of Attenuator Materials

In Table I are listed several characteristics sought for in the materials and designs investigated. It is necessary that the attenuation be high and that the termination shall reflect negligible amounts of the signal. The material should be chemically stable, should not be warped by heat, and should conduct the heat readily from inside the guide to the exterior. For quantity production the characteristics must be readily reproducible and, to minimize the number of necessary materials it is desirable that the one chosen shall be sufficiently adaptable to permit its fabrication in a variety of designs. Since space is often at a premium the material should permit compact designs.

The needs for attenuators made it necessary to examine materials giving a wide range of attenuation values. Low temperature coefficients of attenuation are needed in some cases. In general, water absorption should be minimized since a high attenuation may result due to a strong resonance absorption of radiation by water in the frequency region of interest. Moreover, this attenuation may be variable either because of a change of the number of absorbing molecules in the wave guide or due to a mechanical warpage of the attenuator material out of its normal position in the guide. Also it is sensitive to frequency and temperature.

Classification of Materials Studied on the Basis of Their Physicochemical Structure

From the point of view of the structure there are two general classifications of attenuator materials: Lossy dielectrics and resistor strips.

Homogeneous dielectrics have been used as attenuator materials. They have serious drawbacks, however. Those like polystyrene, methyl methacrylate, and bakelite do not attenuate waves strongly and there are no suitable means of controlling the loss. The materials with higher loss, such as water, exhibit their loss through molecular resonance, resulting in attenuating properties sensi-

Table I. General Characteristics Desirable in Materials and Designs

For terminations
1. High attenuation.
2. Low reflection of energy.
3. Ability to stand high or moderate power without deteriorating.
(a). Good chemical stability.
(b). Good geometrical stability.
(c). High heat conductivity.
4. Good reproducibility.
5. Adaptable from the fabrication point of view.
6. Compact.
Additional characteristics important for attenuators
7. Wide range of attenuation values.
8. Low temperature coefficients of attenuation.
9. Low moisture absorption.

tive to temperature and frequency. The solids described herein avoid some of these drawbacks and, it is believed, more nearly meet the requirements of commercially useful materials. They are dispersions of conducting or semiconducting particles in low loss solid dielectrics. Materials employing ceramic, plastic and rubber bonds have been investigated. The d-c conductivity and other attenuating properties of these materials depend on a number of factors such as: The volume content of semiconductor, the specific resistances of the bond and semiconductor, the particle shape, particle size, dispersion of the particles in the bond, and the methods of making the composite solid.

For the second type of attenuator material cited, a resistor strip, similar combinations of such solids can be employed as films on ceramic, mica, or plastic supports. The bond is normally a plastic.

Although the physical structures of these materials appear very similar the properties obtainable are widely different, the methods of employing them vary considerably and the chemical methods of fabricating them cut across the four rather well established technological fields of ceramics, plastics, rubbers, and finishes.

General Influence of the Dielectric Properties of Solids on Their Application as Attenuator Materials

It is of interest to consider the influence that some of the more fundamental properties of the materials may have on their application. In Figure 2a is shown a metal wave guide filled to the right with a lossy dielectric and to the left with air. A wave traveling to the right is partially reflected on encountering the dielectric while the remainder enters the solid and is attenuated by it. The portion of the wave reflected may be determined from measurements of the maximum and minimum voltages of the standing wave resulting from superposition of the signal and reflected radio frequency. The losses due to dielectric absorption and reflections are important considerations and were subjected to much study. Moreover, reflections that are too small to be serious from the point of view of loss are often of great importance because of the influence they may exert on the mode of operation of a microwave oscillator.

For the disposition of dielectric shown it is possible to determine the impedance of the dielectric filled portion and from this the dielectric constant and tangent of the loss angle. With these data it should be possible, by application of electromagnetic theory, to calculate the reflections and attenuation due to a piece of attenuator material of arbitrary but known geometry in a wave guide. However, due to the difficulties of making measurements of the dielectric constants of high loss materials, because the theory of tapered structures is complex and not well developed, and because the calculations are laborious it was necessary to carry out the present development largely empirically.

It may be helpful, however, to consider the relationships between the fundamental constants of a solid and its suitability as an attenuator material. In Table II are shown some calculated values of loss per wave length for several materials to show the way the magnitudes depend on the tangent of the loss angle and the dielectric constant. In a termination it is necessary that the loss should be of the order of 40 decibels to reduce the signal to negligible magnitude and to prevent appreciable reflections from behind the termination from appearing at the signal generator. For compactness it is desired that this be obtained in as few wave lengths as possible, consistent with meeting the requirements as to reflections. Losses per wave length of 15–20 decibels or greater are desired. Materials such as polystyrene, bakelite, transite, steatite, and porcelain, according to measurements made at 10 centimeters, fall in the lower ranges of loss shown here and are unsatisfactory. Water has a higher loss factor and dielectric constant and gives a loss falling in the higher range indicated in the table but seems unsatisfactory for a variety of reasons, some having been indicated in previous sections. As can be seen, the loss requirements of the materials of interest for terminations must have tan δ values of 0.1 to 1.0 or even more or else have dielectric constant values greater than 40.

The reflection from a lossy dielectric increases with increasing dielectric con-

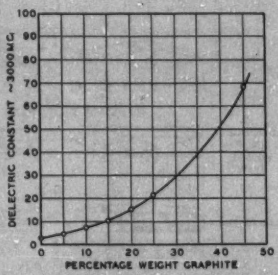


Figure 5. Dependence of the dielectric constant of graphite plus bakelite attenuator material on the graphite content

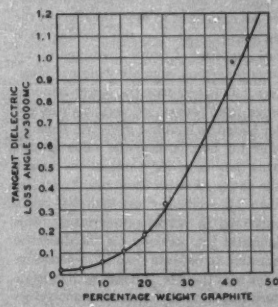


Figure 6. Dependence of dielectric loss of graphite plus bakelite attenuator material on the graphite content

Table II. Dependence of Attenuation per Wave Length on the Loss Tangent and Dielectric Constant

Tan δ	Loss (Db Per Wave Length) ε′ = 4.0	ε′ = 40
0.001	0.057	0.18
0.01	0.57	1.8
0.1	5.7	18.
1.0	49.	157.

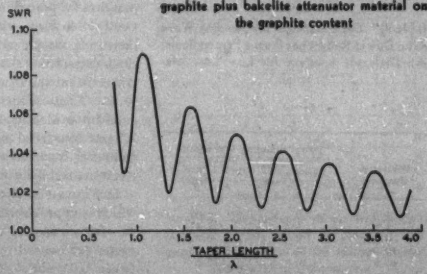


Figure 7. The standing wave ratio as a function of taper length (according to theory of N. H. Frank)

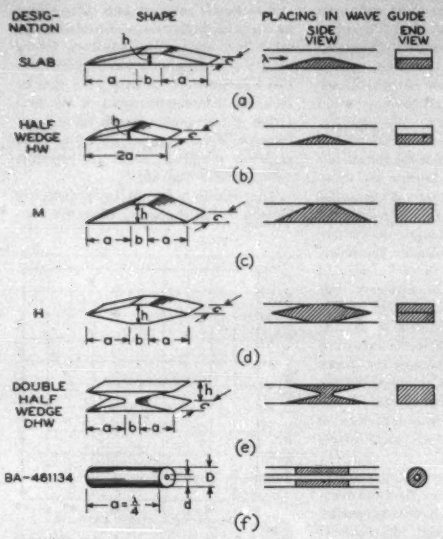


Figure 8 (left). A variety of fixed attenuator pad designs

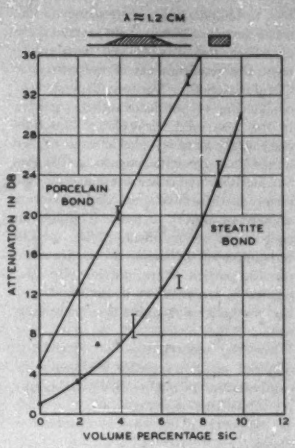


Figure 9. Attenuation of type M-3 attenuators of silicon carbide in porcelain and steatite bonds as a function of the silicon carbide content

stant and tan δ and is more sensitive to changes of dielectric constant than to changes of tan δ. Table III shows some values calculated to show the effect of the dielectric constant on the standing wave ratio in terms of power due to reflections from a perpendicular air–dielectric interface. It is seen that the power standing wave ratio values are very high except for materials having a dielectric constant near 1.0, that is, near the value for air. Most jobs requiring terminations require the power standing wave ratio to be less than 1.5 decibel or less than 1.41 when expressed in simple power ratio. This indicates the necessity for the dielectric constant to be lower than 1.2, whereas we have seen that the materials with satisfactory loss have dielectric constant values 10 to 50 times this. It is therefore necessary in using the lossy materials to alter the geometry of the air–solid interface further in order to reduce the reflections to permissible values. Methods of doing this are discussed under "General Design Features."

Table III. Dependence of the Standing Wave Ratio Due to Reflections From a Perpendicular Air–Dielectric Interface for Low Loss Materials

$$\frac{\lambda}{\epsilon} = 0.7$$

Dielectric Constant	Power Standing Wave Ratio $20 \log \frac{V_{max}}{V_{min}}$ (Db)	$\frac{(V_{max})^2}{(V_{min})}$
1.0	0	1.00
1.2	1.5	1.41
4.0	8.4	6.92
10.0	12.7	18.6
40.0	18.9	77.6

The Dielectric and Thermal Properties of Some Materials Studied

Measurements of several dielectric properties of ceramic and plastic materials were made. One type of material employed has been a ceramic consisting of silicon carbide particles dispersed in porcelain. In Figure 3 is shown plotted the dielectric constant of several of these materials against the frequency in cycles per second.[6] As can be seen the dielectric constant for porcelain is about 6 and practically constant with frequency. For increasing amounts of silicon carbide particles dispersed in the porcelain the dielectric constant increases to as much as 35 to 40. These higher values of dielectric constant evidently are due to Maxwell-Wagner interfacial polarizations resulting from the movement of the conduction electrons in the silicon carbide particles.

In Figure 4 is shown the power factor, which is approximately equal to tan δ in this region, plotted against frequency in cycles per second.[6] The values lie between 0.01 and 0.05 at the lower frequencies but show signs of rapid increase at the highest frequency shown, 60 megacycles, especially for the materials containing the higher percentages of silicon carbide. This is to be expected for such an inhomogeneous material since a maximum loss should occur at some higher frequency, the exact location of the maximum being dependent on the d-c conductivity of the included particles.

Another type of material used quite extensively is a lossy plastic material consisting of graphite particles dispersed in bakelite resin. In Figure 5 one sees that in the case of these materials the dielectric constant as measured at about 3,000 megacycles increases from the low value of about 3 for bakelite to about 70 at 45 per cent graphite by weight. As shown in

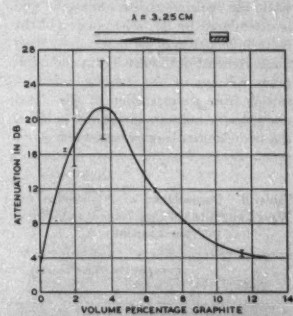


Figure 10. Attenuation of half wedge attenuators of graphite plus porcelain as a function of graphite content

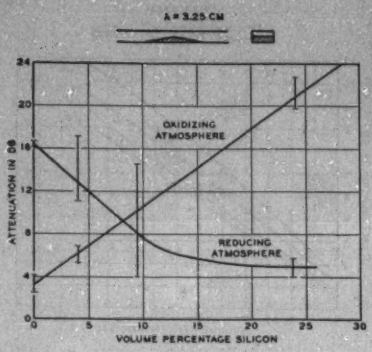


Figure 11 (left). Dependence of the attenuation of half wedge attenuators of silicon plus porcelain on silicon content and furnace atmosphere

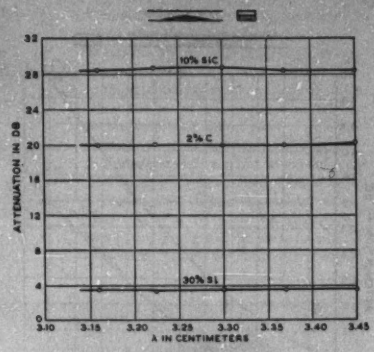


Figure 12 (right). Dependence of the attenuation of porcelain half wedge attenuators on the wave length

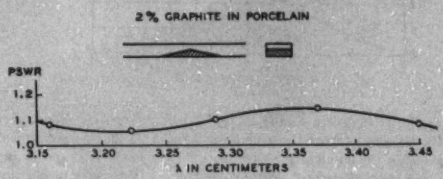


Figure 13. Power standing wave ratio of typical half wedge attenuator as a function of wave length

Figure 6, the tangent of the dielectric loss angle is a somewhat similar function of the graphite content of the attenuator material and reaches a value 1.1 for 45 per cent graphite content. These data show that the loss range for these materials is 0.9 to 219 decibels per wave length.

Another property of the attenuator material which is of considerable importance is the thermal conductivity. For high power applications it is important for the heat to flow readily from the inside to the walls of the wave guide to keep the internal temperatures from being destructive to the material. The generation of heat is greatest where the electric field is highest. Thus in the case of the coaxial conductor where the field builds up logarithmically on approaching the center conductor the heat will be generated most readily near the inner conductor where heat cannot escape so readily as at the outer conductor. In the case of the rectangular guide the electric vector of the dominant wave is perpendicular to the long side of the guide; and has its maximum value along a line perpendicular to the middle of the long edge and drops to zero at the short edges; the heat, therefore, must escape from near the center of the guide to the four faces. In addition to these considerations concerned with the solid in an operating device, in the case of plastic materials it is important in producing strain free moldings that the heat shall flow readily into the material so that curing of the resin is uniform throughout a structure.

Table IV. Effect of Fillers on the Thermal Conductivity of Bakelite

Volume of Filler = 31.7 Per Cent

Filler	Estimated Particle Diameter, Microns	Mean Temp., C	Specific Thermal Conductivity × 10⁴
None		50.0	4.4
Channel black	28	51.2	7.1
Nepheline syenite	2,000–18,000	49.3	9.1
Mica, mineralite 3X	5,000–100,000	46.7	10.6
Silica, fusted	2,000–100,000	48.2	13.5
Acetylene black	43	43.7	14.0
Carbonyl iron	3,000–6,000	43.0	16.5
Copper, lot 2832	500–2,800	50.0	31.2
Copper lot 1598	500–2,800	50.0	46.7
Graphite	2,500–3,000	50.0	104.5

In Table IV are exhibited data showing the effect of various fillers on the thermal conductivity of bakelite. The thermal conductivity is given in the last column. Of the various filler materials measured the graphite filled ones showed by far the highest thermal conductivity. This probably results not only from the high thermal conductivity of graphite but also from its dispersion in the resin and the absence of low conductivity surface films on the granules. One may note that quartz, though a very good electrical insulator, gives an improvement by a factor of about three as compared to pure bakelite molded resin. Though this is a very much lower thermal conductivity than is imparted by the same volume percentage of graphite it was found decidedly advantageous in meeting simultaneously the thermal conductivity, loss, and standing-wave-ratio requirements to employ dispersed silica particles as well as graphite particles in attenuator materials used in high power applications.

General Design Features

Now, as mentioned before, it is necessary in working with these materials to alter the blunt air–solid interface shown in Figure 2a in some manner to reduce the reflections to acceptable values. There are at least two well-known general methods to reduce the reflection of microwaves at air–dielectric interfaces. One of these is to design the end of the dielectric with an approximately quarter wave

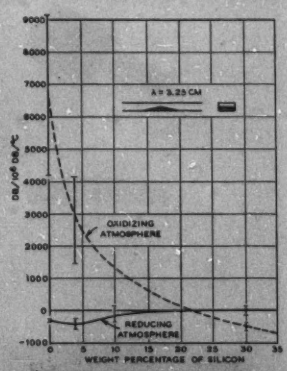


Figure 14. Dependence of the temperature coefficient of attenuation of porcelain half wedges as a function of the silicon content and the furnace atmosphere

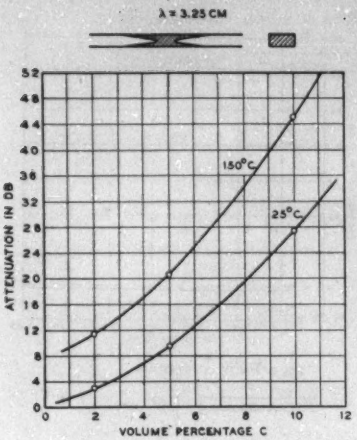
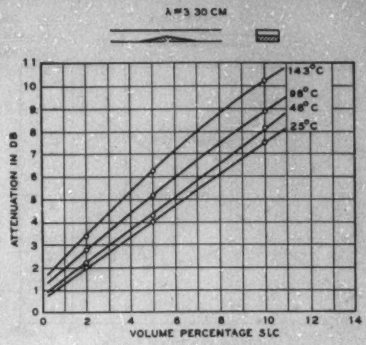


Figure 15 (left). Attenuation of molded bakelite attenuators as a function of graphite content and temperature of measurement

Figure 16 (right). Attenuation of molded bakelite attenuators as a function of silicon carbide content and temperature of measurement



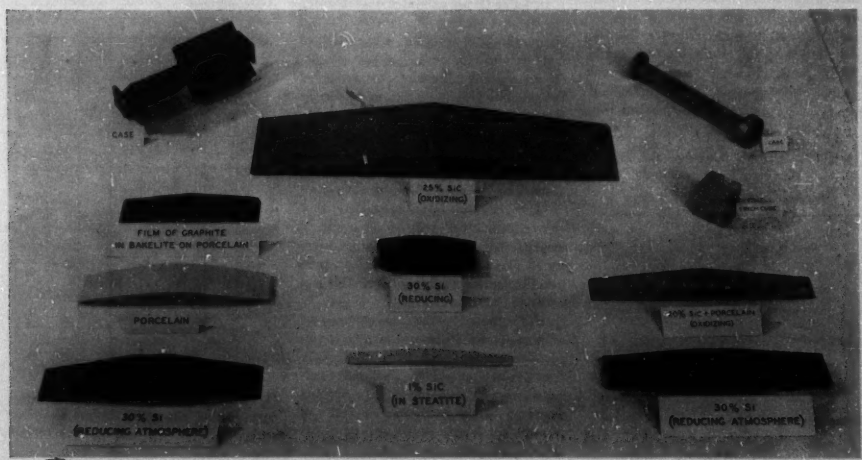
notch, as shown in Figure 2*b*, such that the reflections from the outside and the inside of the notch are 180 degrees out of phase and therefore interfere destructively. This is satisfactory for narrow frequency applications and is feasible from the point of view of fabrication. For wider band applications, however, it is more satisfactory to employ some type of taper such as is shown in the third illustration, Figure 2*c*. While from the electrical point of view it is best to introduce the taper into the radiation field at its weakest point, that is, at the edge of a rectangular guide or at the outside of a coaxial conductor, other considerations such as mechanical properties of the materials and manufacturing methods may enter in and suggest the advisability of adopting some other way. This is true in the case illustrated in Figure 2*c*; the taper is from one wide face to the opposite one. For such a taper the standing wave ratio oscillates through maxima and minima of gradually decreasing magnitudes, as shown in Figure 7.[7,8] Approximately one to seven wave lengths is the usual choice because of the necessity of reducing the space to a minimum. The restriction on taper length is usually greater in the case of attenuators or terminations for the longer wave lengths due to the larger dimensions of the device.

Experimental Results on Materials and Their Application to Microwave Attenuators and Terminations

Application of Lossy Solids to Attenuator Pads

A variety of specific fixed attenuator pad designs was studied as indicated in Figure 8. Most of these were for rectangular wave guides. Various combinations of ceramic and plastic materials and wave guide and coaxial pads were studied.

Figure 17. Examples of ceramic attenuator



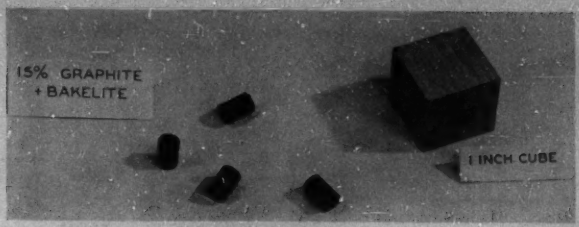


Figure 18. The *BA-461134* "coaxial pad" for the beating oscillator probe

Two of the designs receiving considerable study were the half-wedge type of Figure 8*b* and the *M* type of Figure 8*c*. The first seemed particularly well adapted to the lower attenuation range of 0–20 decibels while the latter was perhaps better for higher values of attenuation.

In Figure 9 are shown curves representing the dependence of the attenuation of *M*-3 pads on the silicon carbide content of porcelain and steatite attenuator materials. As can be seen the attenuation values are lower for the steatite bonded materials. The porcelain materials give

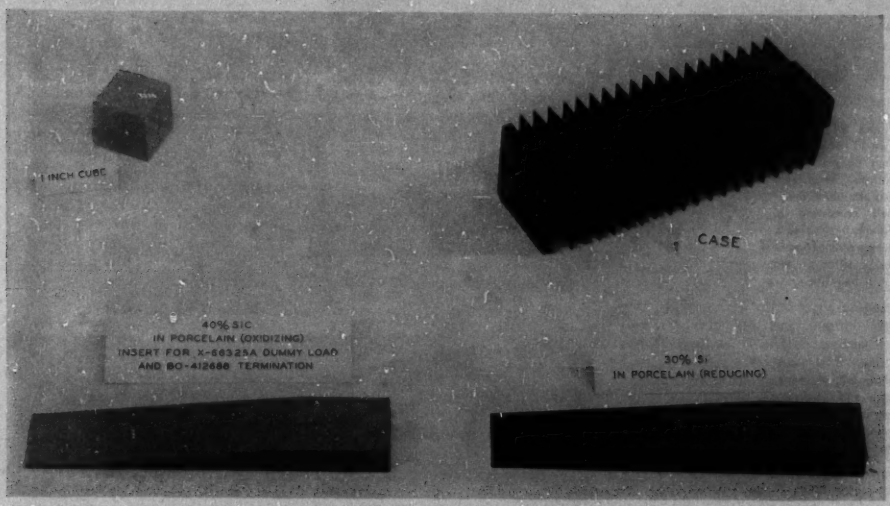


Figure 19. Two ceramic terminations used rather extensively as dummy antennas

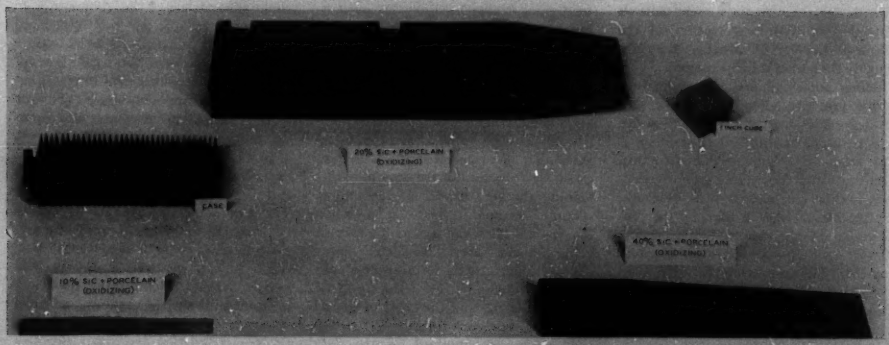


Figure 20. Several other ceramic attenuators designed for specific frequency bands

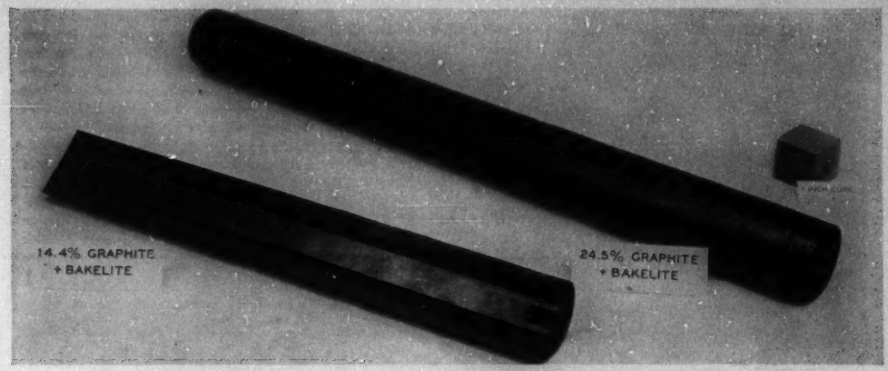


Figure 21 (above). Two 17-inch and 12-inch coaxial terminations employing two attenuator materials consisting of different relative proportions of graphite, silica, and bakelite

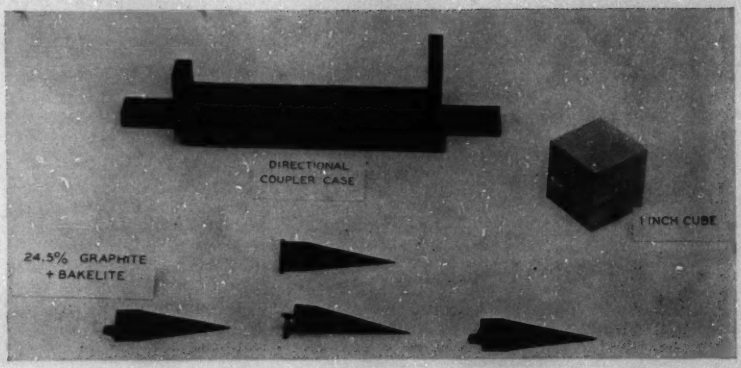


Figure 22. A graphite and bakelite termination developed for absorption of moderate powers

a linear characteristic while the steatite materials give attenuation increasing more rapidly than linearly with silicon carbide content. At the present time we have no adequate explanation of the shape of the curves although they are roughly the sort of things expected.

Some porcelain materials contained added graphite. Figure 10 shows the characteristic obtained for the half-wedge design with increasing content of graphite. The attenuation first increases and then decreases with increasing graphite content. This characteristic is evidently explainable, at least in part, by the conductivity of the material approaching that of a metal with increasing graphite content, thus permitting the wave to go over it with little attenuation since the attenuation for the guide of decreased height is zero so long as the width is maintained constant.

It was found that the characteristic of a ceramic pad not only could be influenced greatly by the bond, the main conductor constituent, and the pad design, but also the temporary binder used for the green ceramic attenuator material. For instance, in Figure 11 it is seen that for a silicon plus porcelain attenuator material fired in a reducing atmosphere the attenuation decreases from 16 decibels to a much lower value with increasing silicon content. The initial high value is due to a small percentage of carbon formed in the porcelain by reduction of the dextrin binder holding the constituents of the unfired ceramic attenuator material together. However, if the firing is carried out in an oxidizing atmosphere the dextrin burns out and quartz films form around each individual silicon particle resulting in d-c specific resistances of the order of 10^{12} to 10^{14} ohm-centimeters instead of 10 to 100 ohm-centimeters obtained in the first case. The resulting characteristic is one that gives increasing attenuation with increasing silicon content.

In Figure 12 is shown the relatively slight dependence of the attenuation on the frequency of the radio-frequency radiation for some of these half-wedge pads. In some cases the results are not this satisfactory but it is of interest that such results are obtainable. Further work is needed to investigate the reproducibility of these results.

In Figure 13 is shown the frequency dependence of the power standing wave ratio of an average ceramic half-wedge. The power standing wave ratio appears to be some periodic function of the frequency, or wavelength as plotted here.

For high power pads it is especially important that the temperature coefficient of attenuation shall be very low, and a pad of very low attenuation to absorb most of the power is often desirable. For this reason the results of Figure 14 on ceramic

half-wedges of silicon plus porcelain are especially interesting. For these materials fired in an oxidizing atmosphere the temperature coefficient for pure porcelain is initially very high, decreases rapidly to zero, and then becomes appreciably negative. For pads fired in a reducing atmosphere the coefficient is initially fairly negative and with increasing silicon content approaches zero.

Pads of varying design were molded from plastic materials. In Figure 15 are shown some results for double half-wedge pads of graphite in bakelite. The lower curve represents the change of attenuation at 25 degrees centigrade with increasing graphite content. For 150 degrees centigrade considerably higher attenuation values are obtained.

In Figure 16 are shown the results for silicon carbide plus bakelite materials as a function of silicon carbide content. The attenuation values are higher the higher the temperature.

Some of the fixed attenuator pads made employing these materials are shown in Figures 17 and 18. The first of these shows several designs utilizing ceramic materials. The second shows a coaxial pad made of a graphite plus phenol formaldehyde material. The latter pad has been used to match a beating oscillator probe into a waveguide.

Application of Lossy Solids to Terminations

As already mentioned, the use of the lossy solids in wave guide or coaxial lines to terminate them and absorb high powers proved to be a most important application. A variety of such terminations were developed in the Laboratories to meet the requirements of specific jobs. In addition to the principles already discussed it was necessary in choosing a combination of design and material to see that unreasonable strains were not created in the material due to too high temperature gradients that might cause physical rupture of the terminations.

Several types of terminations used in microwave equipment and made from the materials already discussed are shown in the following illustrations. In Figure 19 are shown two ceramic terminations that were employed rather extensively as dummy antennae for various 3-centimeter radar systems. The silicon carbide attenuator material represents an improvement over the silicon material in ease and cost of fabrication and stability. In Figure 20 are seen several other ceramic terminations developed for 1-, 6-, and 10-centimeter bands. The larger unit is for 10 centimeters and will dissipate 300 watts average power. Figure 21 shows two coaxial terminations employing two attenuator materials consisting of different relative proportions of graphite, silica, and bakelite. The smaller of the two is shown in longitudinal section. The larger when equipped with a blower and cooling fins will handle a peak power of 750 kw with a duty cycle of about 0.001. Figure 22 shows a graphite and bakelite termination developed for absorption of moderate powers of 1-centimeter waves. One of the uses of this termination is in a directional coupler, the case of which is shown. A typical power standing wave ratio for this termination is 1.03 over the band 24,000 to 25,500 megacycles. Figure 23 shows a molded termination of acetylene black in rubber employed in a 1-centimeter scanner. In this case it was necessary to have a material that would fit snugly in a cavity of complicated shape so as to avoid sparking to the walls.

Application of Film Materials

The second general classification of attenuator materials mentioned was resistor strips. Considerable work was done with sprayed resistor films of graphite pigmented acryloid and graphite pigmented silicone. The first material has been of considerable value as a readily applied lossy material to stop leakage between a metallized radio frequency lens and its enclosing metal horn. The second has been of value in applications where moderate powers of the order of several watts are to be dissipated since the organo-silicon medium will withstand charring at very much higher temperatures than can be tolerated by more conventional organic media. These graphitized silicone films may be employed on ceramic or mica supports. Figure 24 shows such films on mica. These were

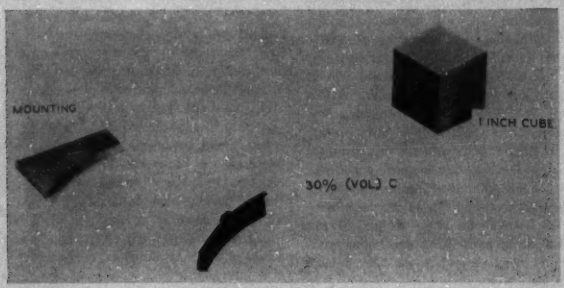


Figure 23 (above). A molded termination of acetylene black in rubber employed in a scanner

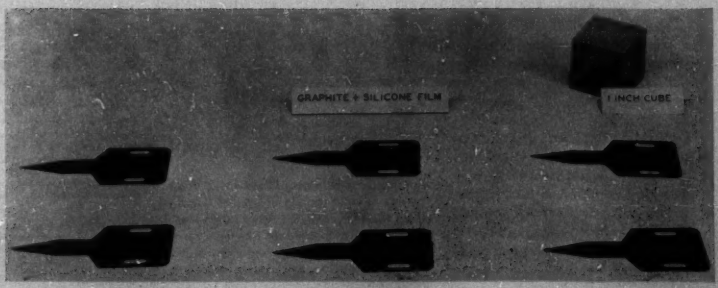


Figure 24. Graphitized silicone films on mica supports

employed satisfactorily as terminations to absorb powers that quickly destroyed commercially available materials.

Conclusion

In conclusion it can be pointed out that the type of material to be recommended must vary with the particular requirements of the job. Ceramic materials are particularly adapted to high-power and wave-guide applications. Plastic materials are particularly valuable for coaxial usage and certain compositions have served admirably for high power absorption. The plastic materials may be somewhat more suited to quantity production and thin tapers than the ceramic type. However, the best high-power attenuators made so far have been with ceramic materials. Film materials are particularly useful for variable attenuators and also have been widely used in pads and terminations. They are cheap and it is relatively easy to fabricate devices from them. Rubber bonded materials are particularly adapted to those applications requiring the attenuating material to fill a cavity of odd design.

The materials and designs discussed here were developed and produced under the urgent conditions incident to the war. They served the purposes for which they were developed very satisfactorily. Several of the designs were produced in relatively large quantities by outside suppliers working to the Laboratories specifications. At the end of the war the large increase in the demands for units and additional designs had led to the formulation of plans for large scale production of additional designs.

In the future it seems likely that interest in this field will center more on materials and designs for precision jobs with less emphasis on the power handling capacity, which was of prime importance in the present development.

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No Discussion

Solutions of the Mathieu Equation

HARRY J. GRAY
NONMEMBER AIEE

RICHARD MERWIN
ASSOCIATE AIEE

J. G. BRAINERD
FELLOW AIEE

IN CIRCUIT THEORY, it is customary, according to one classification, to identify three types of circuits:

(*a*). Linear circuits having constant parameters.

(*b*). Linear circuits having varying parameters.

(*c*). Nonlinear circuits.

Most network theory now existing applies to linear, constant-parameter, circuits (*a*), which are the easiest to handle mathematically and are at the same time the most common ones.

A small amount of work has been done on varying-parameter linear networks (*b*), but for practical purposes the field has not been extensively investigated. Exactly analogous problems have been met in numerous other branches of engineering and physics, and interest has been growing steadily in recent years. The particular purpose of this paper is to present a table of the solutions of an equation which covers one of the simplest cases of a linear circuit containing a varying parameter. The equation has been named by mathematicians the Mathieu Equation, and is given by equation 3. The tables which follow are the first systematic tabulation of solutions to be published. This statement is not meant to apply to tabulations of the periodic solutions. The computation laboratory of the National Bureau of Standards has in preparation extensive tables of these solutions; and previous tables have been published by Stratton, Morse, Chu, and Hutner of the Massachusetts Institute of Technology. W. G. Bickley summarizes work on the Mathieu equation up to 1945[1] and Fletcher, Miller and Rosenhead in their book[2] likewise indicate existing results. These references serve to emphasize the large amount of work done on the periodic solutions, and the lack of work in the nonperiodic solutions, some of which are given in the present paper. It is interesting, however, to note that at least two other agencies were prepared to begin or had begun the computation of similar tables—a commentary on the interest in the results now existing.

The publication of a set of tables of a new function which has application in numerous fields does not have much precedent. Since the present tables represent an analysis of a problem in a little-developed field of electric circuit theory, and since they were obtained by electrical engineers in an electrical engineering institution, it seems reasonable that they should be presented before the AIEE.

An example of a simple varying-parameter circuit might be pictured as a series circuit containing fixed inductance, fixed capacitance, and resistance in series, the resistance being varied periodically or otherwise (the resistor might have an arm connected to a motor or other device, or it might be a carbon-button transmitter the resistance of which is varied by impinging sound waves). The particular varying-parameter circuit of interest here is a simple series circuit containing constant resistance R, constant inductance L, and capacitance C varying in such a way that

$$\frac{1}{C}=\frac{1}{C_o}(1+K\cos\omega\tau)$$

where C_o, ω, and K are constants, $(0<K<1)$, and τ is time. Then i being current,

$$L\frac{di}{d\tau}+Ri+\frac{1+K\cos\omega\tau}{C_o}\int i\,d\tau=0 \qquad (1)$$

This assumes there is no electromotive force in the circuit and that there is some energy stored in the circuit at the time the switch is closed.

Using the instantaneous charge q, equation 1 becomes ($i=dq/d\tau$)

$$L\frac{d^2q}{d\tau^2}+R\frac{dq}{d\tau}+\frac{q}{C_o}(1+K\cos\omega\tau)=0 \qquad (2)$$

By the substitution $q=ye^{-\alpha\tau}$, where $\alpha=R/2L$, the middle term of the equation can be eliminated, and after some slight manipulation, equation 2 reduces to:

$$\frac{d^2y}{dt^2}+\epsilon(1+k\cos t)y=0 \qquad (3)$$

where $\epsilon=(1-R^2C_o/4L)/\omega^2LC_o$ and $k=K/(1-R^2C_o/4L)$, and $t=\omega\tau$. Note that if resistance R can be neglected, $\epsilon=1/\omega^2LC_o$ and $k=K$. Equation 3 is the equation known as Mathieu's equation. As it is the basic equation for the tables which follow, some of the following known characteristics of the equation may be of interest.

Solutions may be stable, periodic, or unstable. A considerable amount of work has been done on the periodic solutions, known as Mathieu functions, because certain physical problems involving equation 3 require that the solution be periodic. This is not the case in the electric circuit problem previously given. An unstable solution is one which ultimately

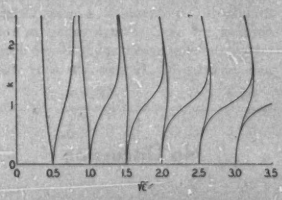

Figure 1. Stable and unstable regions. Any ϵ, k pair falling in a region between two adjacent curves which start from different points on the $k=0$ axis corresponds to a stable solution, in any other region to an unstable solution, on a curve to a periodic solution

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J. G. BRAINERD, professor of electrical engineering, RICHARD MERWIN, research associate and HARRY J. GRAY are all connected with the Moore School of Electrical Engineering, University of Pennsylvania, Philadelphia, Pa.

becomes indefinitely large, and this fact has considerable significance physically. However, for small values of t the distinction between stable and unstable solutions may not be evident. In the tables which follow, both stable and unstable solutions occur. Figure 1 shows the values of ϵ and k for which the various types of solutions may be expected.

If, for given values of ϵ and k, a solution y of equation 3 is known for all values of t between 0 and 2π, the value of y for any finite value of t whatsoever can be found.

If, for given values of ϵ and k, two independent solutions of equation 3 are known for all values of t between 0 and π, the solution of equation 3 can be found for any finite value of t. (Different solutions come from different boundary conditions; in the tables that follow, y is called g for the solution which has initial conditions $g=1$ and $g'(=dg/dt)=0$ at $t=0$; and y is called h for the solution which has initial conditions $h=0$ and $h'=1$ at $t=0$.)

Another way of writing the solution of equation 3 is

$$g=u_1 \cos \mu t - u_2 \sin \mu t$$

and

$$H=u_1 \sin \mu t + u_2 \cos \mu t \tag{4}$$

where u_1 and u_2 are *periodic* functions of t with period 2π (u_1 is an even function and u_2 an odd function about $t=\pi$, and about $t=0$), and μ is a constant called the characteristic exponent, which can be calculated from the values of g and h' at $t=\pi$ by

$$\cos 2\pi\mu = 2g(\pi)h'(\pi)-1$$

The quantity H in (4) is directly proportional to $h(t)$:

$$H=H(t)=Mh(t)$$

where M is the constant $[-g(\pi)g'(\pi)/h(\pi)h'(\pi)]^{1/2}$

Equation 4 can also be written:

$$g=U\cos(\mu t+\theta_0)$$

$$H=U\sin(\mu t+\theta_0)$$

where $U=(u_1^2+u_2^2)^{1/2}$ and $\theta_0=\tan^{-1}(u_1/u_2)$. In this form, y could be interpreted as a wave having both amplitude and frequency modulation (Mathieu's equation occurs in high-frequency work in problems such as the analysis of a super-regenerative amplifier).

The relationships between the periodic u functions and the g and H solutions are:

$$u_1=H\sin\mu t+g\cos\mu t$$

$$u_2=H\cos\mu t-g\sin\mu t \tag{5}$$

whence, since the tables give h, g, μ, and M, these periodic quantities can be found.

The tables which follow tabulate solutions of

$$\frac{d^2y}{dt^2}+\epsilon(1+k\cos t)y=0$$

for $\epsilon=1, 2, 3, \ldots, 10$ and $k=0.1, 0.2, 0.3, \ldots, 1.0$. There are thus 100 tables (one for each ϵ, k combination), and in each table are given g and h for $t=0.1, 0.2, 0.3, \ldots, 3.1$ and for $t=\pi$. The g and h are the solutions of equation 3 for the boundary conditions $g(0)=1$, $g'(0)=0$, and $h(0)=0$, $h'(0)=1$. In addition to g and h, there are given in a separate table the values of μ and M. The tables are given to five decimal places, but because they are extracted from larger ones and no rounding has been done, the digit in the fifth decimal place may be in error, and it seems best to state the accuracy of the tables as four decimal places. In this connection, it may be pointed out that the tables were obtained on the electronic numerical integrator and calculator (ENIAC), using 10-digit numbers and an interval $\Delta t=0.0004$ in the corresponding difference equation. Rounding and truncation errors cut the accuracy so that 5 or 6 figure accuracy might be expected.

During the ENIAC runs, the quantity $gh'-hg'$, which should equal 1 for all values of t, was automatically computed and noted. Any run in which $gh'-hg'$ was out more than 1 in the seventh decimal place was rerun. The computation laboratory of the National Bureau of Standards (Dr. Arnold N. Lowan, Chief) checked by hand several values in the tables, showing agreement to 5 or 6 decimal places. No further checking has been done. To find values of g and h for values of t not given in the tables, calculate u_1 and u_2 by equation 5, and use the expressions for g and H in terms of u_1 and u_2, and μ as previously given.

If it is desired to find a solution of equation 3 when the right-hand side is not zero (this would be the case if an electromotive force were in the circuit), use may be made of the fact that when two independent solutions (such as g and h) of the homogeneous equation 3 [3 is homogeneous] are known, the solution of the non-homogeneous equation [right-hand side not zero in equation 3] can be found. See for example Ince.[3] Equation 19 of reference[4] which can be shown to be Mathieu's equation with right-hand side not zero.

As a matter of interest, it may be noted that in construction of the tables, the ENIAC carried out approximately the equivalent of 7,500,000 multiplications of two 10-digit numbers, and a considerably greater number of additions and subtractions, in a total time of 12 hours. Tables of g' and h' were formed at the same time g and h were tabulated, and although the former (g' and h') are not given here they can be supplied. In the reference to Ince in the preceding paragraph, it may appear that g' and h' are needed, but the fact that $gh'-hg'=1$ for each value of t eliminates this requirement and simplifies greatly the obtaining of a solution when the right-hand side of equation 3 is not zero.

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No Discussion

ε	k	μ	M
1.0	0.1	j 0.00093	j 2.38983
1.0	0.2	j 0.00373	j 2.55270
1.0	0.3	j 0.00838	j 2.72370
1.0	0.4	j 0.01488	j 2.90144
1.0	0.5	j 0.02322	j 3.08409
1.0	0.6	j 0.03336	j 3.26921
1.0	0.7	j 0.04528	j 3.45373
1.0	0.8	j 0.05892	j 3.63397
1.0	0.9	j 0.07420	j 3.80578
1.0	1.0	j 0.09102	j 3.96468
2.0	0.1	1.41320	1.48808
2.0	0.2	1.41011	1.53929
2.0	0.3	1.40480	1.55651
2.0	0.4	1.39713	1.53358
2.0	0.5	1.38694	1.47120
2.0	0.6	1.37412	1.37583
2.0	0.7	1.35863	1.25682
2.0	0.8	1.34044	1.12366
2.0	0.9	1.31953	0.98426
2.0	1.0	1.29583	0.84444
3.0	0.1	1.73087	1.82354
3.0	0.2	1.72726	1.91388
3.0	0.3	1.72106	2.01316
3.0	0.4	1.71193	2.13696
3.0	0.5	1.69921	2.31073
3.0	0.6	1.68174	2.58197
3.0	0.7	1.65728	3.06162
3.0	0.8	1.62058	4.13825
3.0	0.9	1.54618	11.33142
3.0	1.0	1.5 + j 0.11305	-j4.91707
4.0	0.1	1.99866	2.12092
4.0	0.2	1.99460	2.27630
4.0	0.3	1.98768	2.47673
4.0	0.4	1.97768	2.73562
4.0	0.5	1.96432	3.07089
4.0	0.6	1.94727	3.50768
4.0	0.7	1.92613	4.08289
4.0	0.8	1.90045	4.85359
4.0	0.9	1.86961	5.91416
4.0	1.0	1.83272	7.43545
5.0	0.1	2.23459	2.35012
5.0	0.2	2.23011	2.45676
5.0	0.3	2.22246	2.55223
5.0	0.4	2.21130	2.62552
5.0	0.5	2.19602	2.65398
5.0	0.6	2.17542	2.59567
5.0	0.7	2.14692	2.37228
5.0	0.8	2.10315	1.80482
5.0	0.9	j 0.04942	j 0.92479
5.0	1.0	j 0.13499	j 2.65060

ε	k	μ	M
6.0	0.1	2.44789	2.57349
6.0	0.2	2.44303	2.68913
6.0	0.3	2.43473	2.78968
6.0	0.4	2.42268	2.86009
6.0	0.5	2.40630	2.87916
6.0	0.6	2.38474	2.82346
6.0	0.7	2.35659	2.67110
6.0	0.8	2.31959	2.40333
6.0	0.9	2.26954	1.99978
6.0	1.0	2.19612	1.41150
7.0	0.1	2.64403	2.77907
7.0	0.2	2.63882	2.90600
7.0	0.3	2.62992	3.03101
7.0	0.4	2.61700	3.16801
7.0	0.5	2.59940	3.36039
7.0	0.6	2.57549	3.76595
7.0	0.7	2.54087	5.55791
7.0	0.8	2.5 + j 0.04177	-j 3.92124
7.0	0.9	2.5 + j 0.06102	-j 1.64213
7.0	1.0	2.5 + j 0.05697	-j 0.73207
8.0	0.1	2.82660	2.97037
8.0	0.2	2.82105	3.10539
8.0	0.3	2.81159	3.23639
8.0	0.4	2.79787	3.37100
8.0	0.5	2.77925	3.52762
8.0	0.6	2.75465	3.74779
8.0	0.7	2.72190	4.13104
8.0	0.8	2.67596	4.98997
8.0	0.9	2.59801	8.62237
8.0	1.0	2.5 + j 0.12465	-j 6.61421
9.0	0.1	2.99807	3.15024
9.0	0.2	2.99220	3.29541
9.0	0.3	2.98221	3.44363
9.0	0.4	2.96773	3.60952
9.0	0.5	2.94814	3.81722
9.0	0.6	2.92242	4.10634
9.0	0.7	2.88890	4.54660
9.0	0.8	2.84459	5.28109
9.0	0.9	2.78346	6.69625
9.0	1.0	2.68831	10.68295
10.0	0.1	3.16025	3.32009
10.0	0.2	3.15408	3.47004
10.0	0.3	3.14359	3.61208
10.0	0.4	3.12838	3.74053
10.0	0.5	3.10779	3.82919
10.0	0.6	3.08058	3.77334
10.0	0.7	3.04304	3.03711
10.0	0.8	j 0.03272	j 4.69166
10.0	0.9	j 0.01319	j49.02314
10.0	1.0	2.90833	17.27873

t	ε=1.0 g(t)	k=0.1 h(t)	ε=1.0 g(t)	k=0.2 h(t)	ε=1.0 g(t)	k=0.3 h(t)	ε=1.0 g(t)	k=0.4 h(t)	ε=1.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.99450	0.09981	0.99400	0.09980	0.99350	0.09978	0.99300	0.09976	0.99251	0.09975
0.2	0.97808	0.19853	0.97610	0.19840	0.97413	0.19827	0.97215	0.19814	0.97018	0.19800
0.3	0.95093	0.29508	0.94654	0.29464	0.94216	0.29420	0.93778	0.29376	0.93341	0.29332
0.4	0.91338	0.38839	0.90572	0.38737	0.89809	0.38635	0.89047	0.38533	0.88288	0.38431
0.5	0.86586	0.47747	0.85419	0.47552	0.84257	0.47357	0.83099	0.47163	0.81946	0.46970
0.6	0.80893	0.56136	0.79262	0.55809	0.77641	0.55483	0.76029	0.55158	0.74427	0.54834
0.7	0.74325	0.63917	0.72183	0.63416	0.70058	0.62917	0.67950	0.62420	0.65858	0.61925
0.8	0.66958	0.71012	0.64272	0.70293	0.61614	0.69578	0.58983	0.68868	0.56379	0.68162
0.9	0.58875	0.77348	0.55630	0.76370	0.52426	0.75400	0.49263	0.74437	0.46140	0.73481
1.0	0.50167	0.82863	0.46362	0.81590	0.42616	0.80329	0.38928	0.79078	0.35297	0.77838
1.1	0.40930	0.87505	0.36581	0.85907	0.32311	0.84325	0.28119	0.82759	0.24005	0.81209
1.2	0.31266	0.91234	0.26401	0.89287	0.21639	0.87364	0.16978	0.85463	0.12417	0.83584
1.3	0.21279	0.94018	0.15938	0.91712	0.10727	0.89435	0.05642	0.87189	0.00683	0.84973
1.4	0.11072	0.95838	0.05307	0.93170	-0.00301	0.90542	-0.05756	0.87952	-0.11059	0.85400
1.5	0.00754	0.96684	-0.05378	0.93667	-0.11327	0.90698	-0.17094	0.87776	-0.22683	0.84902
1.6	-0.09572	0.96557	-0.16010	0.93214	-0.22238	0.89928	-0.28258	0.86699	-0.34074	0.83525
1.7	-0.19804	0.95468	-0.26484	0.91835	-0.32929	0.88268	-0.39143	0.84765	-0.45130	0.81325
1.8	-0.29840	0.93437	-0.36700	0.89562	-0.43305	0.85759	-0.49658	0.82027	-0.55765	0.78365
1.9	-0.39585	0.90494	-0.46566	0.86435	-0.53277	0.82452	-0.59722	0.78544	-0.65907	0.74710
2.0	-0.48948	0.86676	-0.55998	0.82500	-0.62770	0.78401	-0.69268	0.74378	-0.75497	0.70430
2.1	-0.57841	0.82028	-0.64916	0.77809	-0.71713	0.73664	-0.78237	0.69591	-0.84490	0.65591
2.2	-0.66187	0.76601	-0.73252	0.72419	-0.80049	0.68302	-0.86582	0.64249	-0.92853	0.60262
2.3	-0.73909	0.70454	-0.80942	0.66390	-0.87727	0.62377	-0.94266	0.58416	-1.00561	0.54507
2.4	-0.80943	0.63650	-0.87931	0.59785	-0.94703	0.55954	-1.01259	0.52154	-1.07599	0.48388
2.5	-0.87227	0.56256	-0.94171	0.52672	-1.00942	0.49094	-1.07539	0.45524	-1.13958	0.41964
2.6	-0.92710	0.48345	-0.99621	0.45116	-1.06415	0.41862	-1.13088	0.38585	-1.19634	0.35287
2.7	-0.97346	0.39993	-1.04245	0.37186	-1.11098	0.34318	-1.17894	0.31392	-1.24627	0.28409
2.8	-1.01096	0.31277	-1.08017	0.28951	-1.14971	0.26524	-1.21948	0.23998	-1.28937	0.21375
2.9	-1.03932	0.22278	-1.10912	0.20482	-1.18020	0.18540	-1.25242	0.16454	-1.32564	0.14227
3.0	-1.05830	0.13077	-1.12913	0.11848	-1.20232	0.10425	-1.27770	0.08810	-1.35510	0.07006
3.1	-1.06775	0.03759	-1.14010	0.03118	-1.21600	0.02236	-1.29526	0.01113	-1.37771	-0.00249
π	-1.06885	0.00130	-1.14198	-0.00523	-1.21918	-0.01177	-1.30028	-0.02093	-1.38508	-0.03269

	ε=1.0	k=0.6	ε=1.0	k=0.7	ε=1.0	k=0.8	ε=1.0	k=0.9	ε=1.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.99201	0.09973	0.99151	0.09971	0.99101	0.09970	0.99051	0.09968	0.99002	0.09966
0.2	0.96820	0.19787	0.96623	0.19774	0.96426	0.19761	0.96229	0.19748	0.96033	0.19735
0.3	0.92905	0.29288	0.92469	0.29245	0.92034	0.29201	0.91600	0.29157	0.91166	0.29114
0.4	0.87530	0.38329	0.86775	0.38228	0.86021	0.38127	0.85270	0.38026	0.84520	0.37925
0.5	0.80798	0.46777	0.79655	0.46584	0.78516	0.46392	0.77382	0.46200	0.76253	0.46009
0.6	0.72834	0.54511	0.71251	0.54190	0.69677	0.53870	0.68112	0.53550	0.66557	0.53232
0.7	0.63783	0.61433	0.61724	0.60943	0.59682	0.60455	0.57656	0.59969	0.55646	0.59486
0.8	0.53801	0.67460	0.51250	0.66762	0.48726	0.66068	0.46227	0.65378	0.43755	0.64693
0.9	0.43057	0.72532	0.40014	0.71589	0.37011	0.70654	0.34046	0.69726	0.31121	0.68804
1.0	0.31722	0.76610	0.28204	0.75392	0.24741	0.74184	0.21334	0.72988	0.17981	0.71802
1.1	0.19966	0.79675	0.16004	0.78156	0.12116	0.76654	0.08303	0.75167	0.04563	0.73696
1.2	0.07956	0.81727	0.03592	0.79893	-0.00674	0.78081	-0.04845	0.76290	-0.08922	0.74520
1.3	-0.04152	0.82787	-0.08865	0.80630	-0.13458	0.78501	-0.17932	0.76402	-0.22289	0.74331
1.4	-0.16213	0.82886	-0.21219	0.80410	-0.26081	0.77970	-0.30799	0.75567	-0.35376	0.73201
1.5	-0.28097	0.82073	-0.33338	0.79291	-0.38409	0.76554	-0.43313	0.73862	-0.48052	0.71215
1.6	-0.39690	0.80406	-0.45108	0.77341	-0.50333	0.74330	-0.55368	0.71372	-0.60216	0.68466
1.7	-0.50894	0.77948	-0.56438	0.74633	-0.61768	0.71380	-0.66886	0.68187	-0.71797	0.65053
1.8	-0.61630	0.74772	-0.67257	0.71247	-0.72651	0.67789	-0.77815	0.64399	-0.82755	0.61073
1.9	-0.71835	0.70950	-0.77511	0.67261	-0.82940	0.63644	-0.88127	0.60098	-0.93076	0.56621
2.0	-0.81462	0.66556	-0.87167	0.62755	-0.92617	0.59027	-0.97816	0.55370	-1.02769	0.51785
2.1	-0.90479	0.61662	-0.96206	0.57803	-1.01676	0.54015	-1.06894	0.50296	-1.11863	0.46645
2.2	-0.98866	0.56338	-1.04624	0.52478	-1.10131	0.48680	-1.15390	0.44946	-1.20404	0.41274
2.3	-1.06614	0.50649	-1.12428	0.46843	-1.18004	0.43088	-1.23343	0.39384	-1.28450	0.35732
2.4	-1.13723	0.44656	-1.19632	0.40957	-1.25325	0.37293	-1.30804	0.33664	-1.36068	0.30070
2.5	-1.20198	0.38413	-1.26258	0.34873	-1.32134	0.31345	-1.37824	0.27830	-1.43328	0.24328
2.6	-1.26050	0.31970	-1.32329	0.28635	-1.38467	0.25284	-1.44460	0.21917	-1.50304	0.18537
2.7	-1.31288	0.25372	-1.37870	0.22282	-1.44365	0.19142	-1.50765	0.15954	-1.57064	0.12719
2.8	-1.35926	0.18657	-1.42905	0.15847	-1.49863	0.12947	-1.56789	0.09960	-1.63672	0.06888
2.9	-1.39973	0.11861	-1.47453	0.09358	-1.54991	0.06720	-1.62573	0.03951	-1.70186	0.01053
3.0	-1.43435	0.05015	-1.51528	0.02838	-1.59773	0.00478	-1.68152	-0.02062	-1.76648	-0.04782
3.1	-1.46314	-0.01851	-1.55137	-0.03689	-1.64221	-0.05764	-1.73546	-0.08074	-1.83092	-0.10617
π	-1.47339	-0.04706	-1.56501	-0.06403	-1.65974	-0.08358	-1.75737	-0.10573	-1.85771	-0.13044

t	ε=2.0 g(t)	k=0.1 h(t)	ε=2.0 g(t)	k=0.2 h(t)	ε=2.0 g(t)	k=0.3 h(t)	ε=2.0 g(t)	k=0.4 h(t)	ε=2.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.98902	0.09963	0.98802	0.09960	0.98703	0.09956	0.98603	0.09953	0.98504	0.09950
0.2	0.95633	0.19708	0.95240	0.19681	0.94848	0.19655	0.94457	0.19629	0.94066	0.19603
0.3	0.90268	0.29020	0.89405	0.28933	0.88545	0.28847	0.87687	0.28760	0.86832	0.28673
0.4	0.82929	0.37699	0.81445	0.37498	0.79968	0.37298	0.78500	0.37098	0.77039	0.36900
0.5	0.73782	0.45555	0.71559	0.45176	0.69355	0.44800	0.67168	0.44425	0.65001	0.44051
0.6	0.63033	0.52423	0.59994	0.51795	0.56992	0.51172	0.54026	0.50554	0.51096	0.49939
0.7	0.50921	0.58157	0.47034	0.57210	0.43210	0.56271	0.39449	0.55341	0.35750	0.54420
0.8	0.37715	0.62642	0.32991	0.61308	0.28367	0.59990	0.23842	0.58688	0.19416	0.57401
0.9	0.23703	0.65790	0.18196	0.64012	0.12838	0.62261	0.07626	0.60537	0.02558	0.58838
1.0	0.09188	0.67542	0.02993	0.65280	-0.02995	0.63059	-0.08781	0.60877	-0.14368	0.58736
1.1	-0.05520	0.67873	-0.12277	0.65104	-0.18761	0.62393	-0.24978	0.59741	-0.30934	0.57145
1.2	-0.20114	0.66788	-0.27281	0.63510	-0.34103	0.60313	-0.40588	0.57195	-0.46744	0.54155
1.3	-0.34292	0.64320	-0.41702	0.60556	-0.48691	0.56897	-0.55272	0.53342	-0.61455	0.49888
1.4	-0.47768	0.60535	-0.55247	0.56329	-0.62231	0.52254	-0.68736	0.48309	-0.74777	0.44490
1.5	-0.60274	0.55519	-0.67652	0.50938	-0.74466	0.46515	-0.80737	0.42246	-0.86482	0.38129
1.6	-0.71568	0.49388	-0.78688	0.44515	-0.85185	0.39826	-0.91081	0.35315	-0.96401	0.30978
1.7	-0.81439	0.42272	-0.88162	0.37209	-0.94218	0.32348	-0.99630	0.27686	-1.04424	0.23216
1.8	-0.89704	0.34324	-0.95923	0.29178	-1.01443	0.24249	-1.06292	0.19531	-1.10498	0.15019
1.9	-0.96219	0.25705	-1.01855	0.20590	-1.06781	0.15697	-1.11026	0.11020	-1.14616	0.06555
2.0	-1.00875	0.16589	-1.05885	0.11617	-1.10194	0.06862	-1.13828	0.02317	-1.16816	-0.02019
2.1	-1.03601	0.07156	-1.07976	0.02431	-1.11680	-0.02094	-1.14736	-0.06424	-1.17167	-0.10564
2.2	-1.04362	-0.02413	-1.08129	-0.06798	-1.11274	-0.11015	-1.13815	-0.15066	-1.15769	-0.18952
2.3	-1.03162	-0.11937	-1.06377	-0.15909	-1.09038	-0.19756	-1.11154	-0.23478	-1.12737	-0.27074
2.4	-1.00038	-0.21239	-1.02782	-0.24745	-1.05058	-0.28182	-1.06865	-0.31547	-1.08202	-0.34836
2.5	-0.95065	-0.30148	-0.97437	-0.33160	-0.99443	-0.36170	-1.01069	-0.39172	-1.02301	-0.42159
2.6	-0.88344	-0.38504	-0.90458	-0.41019	-0.92319	-0.43610	-0.93900	-0.46265	-0.95174	-0.48978
2.7	-0.80011	-0.46157	-0.81981	-0.48200	-0.83824	-0.50402	-0.85497	-0.52752	-0.86958	-0.55237
2.8	-0.70224	-0.52971	-0.72162	-0.54592	-0.74107	-0.56461	-0.76002	-0.58565	-0.77789	-0.60892
2.9	-0.59166	-0.58827	-0.61173	-0.60098	-0.63328	-0.61710	-0.65561	-0.63650	-0.67797	-0.65903
3.0	-0.47041	-0.63623	-0.49198	-0.64638	-0.51652	-0.66086	-0.54317	-0.67956	-0.57106	-0.70236
3.1	-0.34070	-0.67273	-0.36436	-0.68142	-0.39250	-0.69533	-0.42418	-0.71442	-0.45839	-0.73860
π	-0.28481	-0.68439	-0.30944	-0.69281	-0.33920	-0.70683	-0.37310	-0.72641	-0.41011	-0.75151

	ε=2.0	k=0.6	ε=2.0	k=0.7	ε=2.0	k=0.8	ε=2.0	k=0.9	ε=2.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.98404	0.09946	0.98305	0.09943	0.98206	0.09940	0.98106	0.09936	0.98007	0.09933
0.2	0.93675	0.19577	0.93285	0.19550	0.92896	0.19524	0.92507	0.19498	0.92118	0.19472
0.3	0.85980	0.28587	0.85129	0.28501	0.84282	0.28415	0.83437	0.28329	0.82595	0.28243
0.4	0.75586	0.36701	0.74141	0.36504	0.72703	0.36307	0.71274	0.36111	0.69852	0.35915
0.5	0.62851	0.43680	0.60719	0.43310	0.58606	0.42943	0.56511	0.42577	0.54433	0.42213
0.6	0.48201	0.49329	0.45342	0.48723	0.42518	0.48122	0.39729	0.47524	0.36975	0.46931
0.7	0.32112	0.53508	0.28536	0.52604	0.25020	0.51709	0.21564	0.50822	0.18167	0.49943
0.8	0.15087	0.56130	0.10853	0.54874	0.06715	0.53634	0.02670	0.52409	-0.01282	0.51199
0.9	-0.02367	0.57165	-0.07153	0.55517	-0.11802	0.53894	-0.16314	0.52296	-0.20694	0.50723
1.0	-0.19760	0.56633	-0.24959	0.54570	-0.29970	0.52544	-0.34796	0.50556	-0.39441	0.48606
1.1	-0.36633	0.54606	-0.42082	0.52122	-0.47287	0.49693	-0.52252	0.47318	-0.56982	0.44995
1.2	-0.52580	0.51192	-0.58103	0.48304	-0.63321	0.45490	-0.68243	0.42748	-0.72876	0.40079
1.3	-0.67252	0.46533	-0.72673	0.43276	-0.77730	0.40114	-0.82434	0.37047	-0.86794	0.34071
1.4	-0.80368	0.40795	-0.85524	0.37221	-0.90259	0.33766	-0.94587	0.30426	-0.98520	0.27200
1.5	-0.91720	0.34159	-0.96468	0.30334	-1.00745	0.26649	-1.04566	0.23103	-1.07950	0.19691
1.6	-1.01164	0.26811	-1.05393	0.22809	-1.09108	0.18969	-1.12328	0.15287	-1.15075	0.11758
1.7	-1.08626	0.18934	-1.12258	0.14836	-1.15345	0.10917	-1.17908	0.07171	-1.19971	0.03595
1.8	-1.14087	0.10708	-1.17085	0.06592	-1.19517	0.02667	-1.21407	-0.01072	-1.22781	-0.04632
1.9	-1.17581	0.02295	-1.19946	-0.01763	-1.21736	-0.05628	-1.22978	-0.09302	-1.23695	-0.12791
2.0	-1.19182	-0.06155	-1.20953	-0.10095	-1.22153	-0.13842	-1.22806	-0.17403	-1.22937	-0.20780
2.1	-1.18997	-0.14516	-1.20247	-0.18285	-1.20940	-0.21874	-1.21099	-0.25288	-1.20744	-0.28529
2.2	-1.17153	-0.22676	-1.17987	-0.26240	-1.18287	-0.29647	-1.18070	-0.32899	-1.17353	-0.35997
2.3	-1.13795	-0.30544	-1.14339	-0.33889	-1.14380	-0.37108	-1.13929	-0.40202	-1.12996	-0.43171
2.4	-1.09070	-0.38046	-1.09470	-0.41177	-1.09404	-0.44224	-1.08874	-0.47185	-1.07882	-0.50059
2.5	-1.03129	-0.45126	-1.03542	-0.48067	-1.03530	-0.50978	-1.03085	-0.53852	-1.02200	-0.56684
2.6	-0.96116	-0.51738	-0.96702	-0.54537	-0.96910	-0.57366	-0.96719	-0.60218	-0.96108	-0.63085
2.7	-0.88168	-0.57847	-0.89087	-0.60570	-0.89679	-0.63395	-0.89908	-0.66310	-0.89739	-0.69305
2.8	-0.79412	-0.63427	-0.80817	-0.66159	-0.81950	-0.69072	-0.82760	-0.72154	-0.83196	-0.75392
2.9	-0.69965	-0.68457	-0.71995	-0.71297	-0.73816	-0.74409	-0.75358	-0.77779	-0.76554	-0.81392
3.0	-0.59934	-0.72914	-0.62711	-0.75978	-0.65351	-0.79413	-0.67765	-0.83206	-0.69866	-0.87343
3.1	-0.49415	-0.76780	-0.53041	-0.80192	-0.56613	-0.84086	-0.60023	-0.88452	-0.63163	-0.93276
π	-0.44918	-0.78208	-0.48921	-0.81804	-0.52909	-0.85931	-0.56766	-0.90581	-0.60374	-0.95742

t	ε=3.0 g(t)	k=0.1 h(t)	ε=3.0 g(t)	k=0.2 h(t)	ε=3.0 g(t)	k=0.3 h(t)	ε=3.0 g(t)	k=0.4 h(t)	ε=3.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.98354	0.09945	0.98205	0.09940	0.98056	0.09934	0.97907	0.09930	0.97759	0.09925
0.2	0.93474	0.19563	0.92889	0.19523	0.92306	0.19484	0.91724	0.19445	0.91143	0.19406
0.3	0.85523	0.28538	0.84251	0.28409	0.82985	0.28280	0.81724	0.28152	0.80470	0.28024
0.4	0.74769	0.36578	0.72611	0.36283	0.70471	0.35989	0.68348	0.35696	0.66242	0.35405
0.5	0.61572	0.43424	0.58399	0.42872	0.55266	0.42324	0.52174	0.41781	0.49121	0.41241
0.6	0.46371	0.48856	0.42133	0.47954	0.37974	0.47062	0.33892	0.46179	0.29887	0.45305
0.7	0.29668	0.52707	0.24398	0.51366	0.19263	0.50044	0.14260	0.48740	0.09387	0.47456
0.8	0.12008	0.54859	0.05820	0.53006	-0.00157	0.51186	-0.05931	0.49400	-0.11502	0.47647
0.9	-0.06035	0.55257	-0.12956	0.52839	-0.19575	0.50477	-0.25898	0.48170	-0.31933	0.45917
1.0	-0.23888	0.53898	-0.31299	0.50896	-0.38300	0.47978	-0.44902	0.45142	-0.51116	0.42387
1.1	-0.40989	0.50839	-0.48606	0.47265	-0.55696	0.43810	-0.62276	0.40471	-0.68362	0.37246
1.2	-0.56808	0.46190	-0.64329	0.42092	-0.71202	0.38153	-0.77452	0.34369	-0.83103	0.30738
1.3	-0.70867	0.40109	-0.77989	0.35567	-0.84348	0.31229	-0.89978	0.27089	-0.94912	0.23142
1.4	-0.82748	0.32795	-0.89191	0.27921	-0.94770	0.23295	-0.99527	0.18911	-1.03505	0.14759
1.5	-0.92113	0.24483	-0.97636	0.19410	-1.02213	0.14628	-1.05897	0.10126	-1.08739	0.05896
1.6	-0.98701	0.15432	-1.03118	0.10309	-1.06533	0.05512	-1.09010	0.01028	-1.10606	-0.03152
1.7	-1.02345	0.05920	-1.05532	0.00900	-1.07694	-0.03768	-1.08898	-0.08101	-1.09211	-0.12110
1.8	-1.02966	-0.03766	-1.04870	-0.08535	-1.05756	-0.12942	-1.05696	-0.17003	-1.04757	-0.20731
1.9	-1.00575	-0.13343	-1.01211	-0.17727	-1.00868	-0.21756	-0.99615	-0.25444	-0.97521	-0.28804
2.0	-0.95271	-0.22534	-0.94718	-0.26425	-0.93252	-0.29983	-0.90937	-0.33223	-0.87836	-0.36156
2.1	-0.87234	-0.31080	-0.85626	-0.34398	-0.83192	-0.37426	-0.79988	-0.40174	-0.76065	-0.42652
2.2	-0.76718	-0.38742	-0.74227	-0.41446	-0.71017	-0.43919	-0.67124	-0.46167	-0.62588	-0.48194
2.3	-0.64040	-0.45313	-0.60868	-0.47399	-0.57089	-0.49329	-0.52721	-0.51103	-0.47785	-0.52718
2.4	-0.49572	-0.50619	-0.45928	-0.52123	-0.41791	-0.53559	-0.37158	-0.54916	-0.32025	-0.56189
2.5	-0.33730	-0.54521	-0.29814	-0.55517	-0.25518	-0.56539	-0.20808	-0.57571	-0.15655	-0.58598
2.6	-0.16958	-0.56922	-0.12950	-0.57515	-0.08662	-0.58233	-0.04032	-0.59053	0.00997	-0.59955
2.7	0.00278	-0.57766	0.04236	-0.58086	0.08387	-0.58631	0.12824	-0.59373	0.17635	-0.60284
2.8	0.17507	-0.57036	0.21319	-0.57233	0.25255	-0.57750	0.29438	-0.58557	0.33985	-0.59623
2.9	0.34262	-0.54760	0.37885	-0.54988	0.41581	-0.55627	0.45502	-0.56647	0.49797	-0.58016
3.0	0.50091	-0.51004	0.53537	-0.51416	0.57025	-0.52323	0.60733	-0.53699	0.64842	-0.55515
3.1	0.64569	-0.45872	0.67903	-0.46610	0.71268	-0.47917	0.74866	-0.49778	0.78906	-0.52172
π	0.70099	-0.43364	0.73418	-0.44274	0.76768	-0.45783	0.80361	-0.47879	0.84418	-0.50548

	ε=3.0	k=0.6	ε=3.0	k=0.7	ε=3.0	k=0.8	ε=3.0	k=0.9	ε=3.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.97610	0.09920	0.97461	0.09915	0.97313	0.09910	0.97164	0.09905	0.97016	0.09900
0.2	0.90564	0.19367	0.89985	0.19328	0.89408	0.19289	0.88831	0.19250	0.88256	0.19211
0.3	0.79221	0.27896	0.77977	0.27769	0.76740	0.27642	0.75508	0.27515	0.74281	0.27389
0.4	0.64153	0.35115	0.62082	0.34826	0.60028	0.34539	0.57990	0.34253	0.55970	0.33969
0.5	0.46108	0.40705	0.43134	0.40174	0.40199	0.39646	0.37303	0.39122	0.34445	0.38603
0.6	0.25958	0.44440	0.22104	0.43585	0.18324	0.42739	0.14618	0.41902	0.10985	0.41074
0.7	0.04644	0.46190	0.00027	0.44942	-0.04464	0.43713	-0.08833	0.42501	-0.13080	0.41307
0.8	-0.16876	0.45926	-0.22055	0.44238	-0.27044	0.42582	-0.31846	0.40957	-0.36464	0.39364
0.9	-0.37686	0.43716	-0.43163	0.41567	-0.48372	0.39469	-0.53318	0.37422	-0.58009	0.35425
1.0	-0.56954	0.39711	-0.62426	0.37112	-0.67542	0.34590	-0.72314	0.32142	-0.76751	0.29768
1.1	-0.73972	0.34132	-0.79122	0.31127	-0.83828	0.28228	-0.88105	0.25432	-0.91969	0.22738
1.2	-0.88180	0.27254	-0.92706	0.23913	-0.96703	0.20713	-1.00193	0.17649	-1.03198	0.14718
1.3	-0.99182	0.19381	-1.02819	0.15801	-1.05853	0.12397	-1.08314	0.09163	-1.10229	0.06094
1.4	-1.06744	0.10833	-1.09284	0.07125	-1.11163	0.03627	-1.12418	0.00333	-1.13084	-0.02764
1.5	-1.10788	0.01926	-1.12093	-0.01792	-1.12697	-0.05268	-1.12647	-0.08511	-1.11983	-0.11530
1.6	-1.11378	-0.07044	-1.11382	-0.10656	-1.10669	-0.14002	-1.09290	-0.17090	-1.07293	-0.19932
1.7	-1.08693	-0.15810	-1.07405	-0.19212	-1.05404	-0.22329	-1.02745	-0.25174	-0.99482	-0.27758
1.8	-1.03004	-0.24141	-1.00501	-0.27247	-0.97306	-0.30060	-0.93479	-0.32596	-0.89073	-0.34864
1.9	-0.94650	-0.31851	-0.91063	-0.34598	-0.86821	-0.37058	-0.81980	-0.39243	-0.76596	-0.41167
2.0	-0.84008	-0.38794	-0.79512	-0.41150	-0.74403	-0.43235	-0.68735	-0.45060	-0.62560	-0.46637
2.1	-0.71476	-0.44867	-0.66269	-0.46830	-0.60494	-0.48550	-0.54196	-0.50035	-0.47422	-0.51294
2.2	-0.57447	-0.50005	-0.51738	-0.51605	-0.45498	-0.52999	-0.38765	-0.54193	-0.31574	-0.55191
2.3	-0.42301	-0.54174	-0.36290	-0.55471	-0.29776	-0.56609	-0.22781	-0.57588	-0.15329	-0.58408
2.4	-0.26391	-0.57369	-0.20258	-0.58451	-0.13632	-0.59427	-0.06517	-0.60292	0.01074	-0.61040
2.5	-0.10036	-0.59607	-0.03928	-0.60583	0.02684	-0.61514	0.09815	-0.62387	0.17476	-0.63192
2.6	0.06477	-0.60916	0.12456	-0.61917	0.18975	-0.62938	0.26072	-0.63961	0.33778	-0.64967
2.7	0.22899	-0.61338	0.28695	-0.62508	0.35090	-0.63768	0.42152	-0.65094	0.49938	-0.66461
2.8	0.39009	-0.60918	0.44619	-0.62410	0.50917	-0.64069	0.57999	-0.65863	0.65957	-0.67763
2.9	0.54612	-0.59703	0.60090	-0.61674	0.66368	-0.63895	0.73583	-0.66331	0.81863	-0.68946
3.0	0.69532	-0.57741	0.74983	-0.60345	0.81376	-0.63292	0.88889	-0.66546	0.97697	-0.70068
3.1	0.83606	-0.55075	0.89186	-0.58459	0.95875	-0.62293	1.03903	-0.66541	1.13501	-0.71167
π	0.89172	-0.53769	0.94864	-0.57518	1.01740	-0.61766	1.10056	-0.66480	1.20072	-0.71623

t	ε=4.0 g(t)	k=0.1 h(t)	ε=4.0 g(t)	k=0.2 h(t)	ε=4.0 g(t)	k=0.3 h(t)	ε=4.0 g(t)	k=0.4 h(t)	ε=4.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.97808	0.09926	0.97609	0.09920	0.97411	0.09913	0.97213	0.09906	0.97015	0.09900
0.2	0.91330	0.19418	0.90557	0.19366	0.89786	0.19314	0.89017	0.19262	0.88250	0.19211
0.3	0.80857	0.28061	0.79190	0.27890	0.77534	0.27721	0.75888	0.27551	0.74252	0.27383
0.4	0.66852	0.35478	0.64065	0.35091	0.61309	0.34707	0.58582	0.34325	0.55886	0.33946
0.5	0.49938	0.41352	0.45917	0.40637	0.41966	0.39930	0.38083	0.39230	0.34269	0.38536
0.6	0.30859	0.45433	0.25618	0.44280	0.20510	0.43144	0.15533	0.42025	0.10686	0.40921
0.7	0.10447	0.47554	0.04127	0.45868	-0.01967	0.44215	-0.07842	0.42594	-0.13501	0.41005
0.8	-0.10413	0.47635	-0.17554	0.45349	-0.24351	0.43121	-0.30814	0.40949	-0.36951	0.38832
0.9	-0.30830	0.45685	-0.38441	0.42771	-0.45566	0.39949	-0.52222	0.37217	-0.58423	0.34573
1.0	-0.49944	0.41801	-0.57608	0.38277	-0.64630	0.34889	-0.71037	0.31634	-0.76851	0.28509
1.1	-0.66960	0.36160	-0.74233	0.32091	-0.80705	0.28212	-0.86414	0.24518	-0.91396	0.21001
1.2	-0.81188	0.29012	-0.87633	0.24510	-0.93129	0.20257	-0.97728	0.16245	-1.01479	0.12464
1.3	-0.92062	0.20666	-0.97289	0.15881	-1.01440	0.11406	-1.04585	0.07229	-1.06790	0.03338
1.4	-0.99169	0.11473	-1.02861	0.06583	-1.05386	0.02061	-1.06830	-0.02107	-1.07276	-0.05941
1.5	-1.02256	0.01814	-1.04193	-0.02986	-1.04917	-0.07370	-1.04528	-0.11357	-1.03124	-0.14967
1.6	-1.01238	-0.07917	-1.01313	-0.12437	-1.00176	-0.16505	-0.97940	-0.20144	-0.94714	-0.23378
1.7	-0.96195	-0.17335	-0.94417	-0.21396	-0.91475	-0.24989	-0.87491	-0.28139	-0.82579	-0.30873
1.8	-0.87366	-0.26071	-0.83852	-0.29525	-0.79266	-0.32516	-0.73729	-0.35072	-0.67358	-0.37219
1.9	-0.75132	-0.33792	-0.70094	-0.36532	-0.64108	-0.38837	-0.57290	-0.40735	-0.49751	-0.42251
2.0	-0.59998	-0.40210	-0.53720	-0.42176	-0.46639	-0.43760	-0.38857	-0.44984	-0.30474	-0.45871
2.1	-0.42570	-0.45092	-0.35379	-0.46279	-0.27537	-0.47156	-0.19127	-0.47739	-0.10228	-0.48042
2.2	-0.23530	-0.48266	-0.15768	-0.48723	-0.07501	-0.48956	0.01215	-0.48973	0.10328	-0.48780
2.3	-0.03605	-0.49629	0.04400	-0.49452	0.12784	-0.49149	0.21526	-0.48714	0.30600	-0.48144
2.4	0.16453	-0.49145	0.24418	-0.48472	0.32666	-0.47771	0.41211	-0.47028	0.50062	-0.46226
2.5	0.35906	-0.46845	0.43608	-0.45843	0.51534	-0.44909	0.59739	-0.44017	0.68266	-0.43142
2.6	0.54043	-0.42827	0.61338	-0.41678	0.68843	-0.40685	0.76649	-0.39812	0.84839	-0.39024
2.7	0.70209	-0.37247	0.77042	-0.36134	0.84111	-0.35254	0.91550	-0.34562	0.99477	-0.34014
2.8	0.83829	-0.30315	0.90228	-0.29409	0.96934	-0.28797	1.04118	-0.28430	1.11940	-0.28259
2.9	0.94421	-0.22288	1.00493	-0.21732	1.06983	-0.21515	1.14097	-0.21590	1.22038	-0.21906
3.0	1.01613	-0.13459	1.07528	-0.13356	1.14005	-0.13624	1.21289	-0.14222	1.29627	-0.15102
3.1	1.05153	-0.04145	1.11121	-0.04552	1.17827	-0.05350	1.25555	-0.06510	1.34600	-0.07993
π	1.05516	-0.00208	1.11574	-0.00831	1.18450	-0.01851	1.26446	-0.03246	1.35880	-0.04980

	ε=4.0	k=0.6	ε=4.0	k=0.7	ε=4.0	k=0.8	ε=4.0	k=0.9	ε=4.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.96818	0.09893	0.96620	0.09887	0.96422	0.09880	0.96225	0.09873	0.96028	0.09867
0.2	0.87485	0.19159	0.86722	0.19107	0.85962	0.19056	0.85203	0.19004	0.84446	0.18953
0.3	0.72626	0.27215	0.71010	0.27048	0.69404	0.26881	0.67808	0.26715	0.66221	0.26549
0.4	0.53220	0.33569	0.50583	0.33194	0.47975	0.32822	0.45397	0.32452	0.42848	0.32085
0.5	0.30523	0.37850	0.26843	0.37171	0.23229	0.36498	0.19682	0.35832	0.16199	0.35173
0.6	0.05967	0.39833	0.01374	0.38761	-0.03094	0.37704	-0.07441	0.36663	-0.11667	0.35637
0.7	-0.18948	0.39446	-0.24187	0.37919	-0.29222	0.36421	-0.34058	0.34954	-0.38698	0.33516
0.8	-0.42770	0.36769	-0.48278	0.34760	-0.53485	0.32802	-0.58399	0.30897	-0.63025	0.29042
0.9	-0.64184	0.32015	-0.69520	0.29541	-0.74444	0.27150	-0.78970	0.24838	-0.83111	0.22606
1.0	-0.82097	0.25510	-0.86798	0.22634	-0.90975	0.19877	-0.94651	0.17236	-0.97846	0.14708
1.1	-0.95687	0.17657	-0.99319	0.14481	-1.02326	0.11467	-1.04740	0.08610	-1.06592	0.05905
1.2	-1.04433	0.08907	-1.06635	0.05565	-1.08130	0.02431	-1.08963	-0.00503	-1.09174	-0.03245
1.3	-1.08117	-0.00277	-1.08628	-0.03631	-1.08381	-0.06733	-1.07433	-0.09595	-1.05836	-0.12225
1.4	-1.06803	-0.09454	-1.05483	-0.12661	-1.03390	-0.15578	-1.00591	-0.18218	-0.97151	-0.20594
1.5	-1.00797	-0.18218	-0.97633	-0.21131	-0.93716	-0.23722	-0.89123	-0.26009	-0.83930	-0.28008
1.6	-0.90599	-0.26229	-0.85693	-0.28720	-0.80088	-0.30871	-0.73869	-0.32701	-0.67118	-0.34231
1.7	-0.76848	-0.33216	-0.70401	-0.35191	-0.63334	-0.36821	-0.55737	-0.38128	-0.47696	-0.39133
1.8	-0.60263	-0.38982	-0.52546	-0.40387	-0.44304	-0.41457	-0.35628	-0.42213	-0.26603	-0.42678
1.9	-0.41595	-0.43408	-0.32920	-0.44231	-0.23818	-0.44741	-0.14376	-0.44960	-0.04675	-0.44909
2.0	-0.21582	-0.46439	-0.12268	-0.46710	-0.02617	-0.46703	0.07294	-0.46436	0.17392	-0.45927
2.1	-0.00915	-0.48080	0.08738	-0.47869	0.18664	-0.47421	0.28795	-0.46752	0.39067	-0.45874
2.2	0.19784	-0.48384	0.29529	-0.47792	0.39511	-0.47011	0.49679	-0.46049	0.59982	-0.44913
2.3	0.39979	-0.47437	0.49633	-0.46592	0.59531	-0.45606	0.69638	-0.44479	0.79920	-0.43213
2.4	0.59221	-0.45353	0.68686	-0.44398	0.78447	-0.43349	0.88492	-0.42197	0.98801	-0.40934
2.5	0.77150	-0.42258	0.86416	-0.41344	0.96084	-0.40380	1.06161	-0.39345	1.16650	-0.38223
2.6	0.93481	-0.38285	1.02634	-0.37564	1.12345	-0.36829	1.22651	-0.36053	1.33576	-0.35207
2.7	1.08000	-0.33568	1.17213	-0.33181	1.27197	-0.32814	1.38020	-0.32429	1.49740	-0.31986
2.8	1.20546	-0.28235	1.30072	-0.28310	1.40641	-0.28435	1.52362	-0.28561	1.65329	-0.28642
2.9	1.30998	-0.22411	1.41161	-0.23052	1.52701	-0.23774	1.65776	-0.24519	1.80535	-0.25229
3.0	1.39264	-0.16213	1.50442	-0.17499	1.63395	-0.18899	1.78352	-0.20351	1.95527	-0.21786
3.1	1.45270	-0.09751	1.57874	-0.11730	1.72728	-0.13867	1.90145	-0.16094	2.10436	-0.18333
π	1.47086	-0.07011	1.60409	-0.09284	1.76203	-0.11739	1.94825	-0.14303	2.16632	-0.16896

t	ε=5.0 g(t)	k=0.1 h(t)	ε=5.0 g(t)	k=0.2 h(t)	ε=5.0 g(t)	k=0.3 h(t)	ε=5.0 g(t)	k=0.4 h(t)	ε=5.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.97262	0.09908	0.97015	0.09900	0.96768	0.09892	0.96521	0.09883	0.96274	0.09875
0.2	0.89203	0.19275	0.88244	0.19210	0.87288	0.19145	0.86336	0.19081	0.85386	0.19016
0.3	0.76269	0.27588	0.74222	0.27377	0.72192	0.27167	0.70177	0.26959	0.68177	0.26751
0.4	0.59176	0.34397	0.55802	0.33923	0.52476	0.33452	0.49195	0.32985	0.45961	0.32522
0.5	0.38866	0.39337	0.34094	0.38470	0.29427	0.37614	0.24865	0.36769	0.20405	0.35934
0.6	0.16451	0.42147	0.10389	0.40768	0.04527	0.39414	-0.01139	0.38084	-0.06614	0.36778
0.7	-0.06851	0.42687	-0.13919	0.40703	-0.20657	0.38768	-0.27073	0.36880	-0.33175	0.35039
0.8	-0.29788	0.40939	-0.37432	0.38303	-0.44582	0.35751	-0.51255	0.33281	-0.57464	0.30893
0.9	-0.51139	0.37012	-0.58826	0.33731	-0.65831	0.30583	-0.72183	0.27565	-0.77908	0.24674
1.0	-0.69789	0.31127	-0.76931	0.27271	-0.83197	0.23609	-0.88633	0.20136	-0.93281	0.16844
1.1	-0.84778	0.23608	-0.90796	0.19306	-0.95757	0.15269	-0.99726	0.11486	-1.02766	0.07948
1.2	-0.95356	0.14861	-0.99733	0.10292	-1.02905	0.06063	-1.04960	0.02158	-1.05983	-0.01435
1.3	-1.01016	0.05346	-1.03348	0.00727	-1.04377	-0.03479	-1.04216	-0.07294	-1.02973	-0.10739
1.4	-1.01512	-0.04442	-1.01544	-0.08877	-1.00236	-0.12837	-0.97728	-0.16349	-0.94150	-0.19441
1.5	-0.96868	-0.14007	-0.94511	-0.18026	-0.90850	-0.21525	-0.86043	-0.24537	-0.80239	-0.27096
1.6	-0.87366	-0.22871	-0.82705	-0.26267	-0.76841	-0.29121	-0.69948	-0.31472	-0.62185	-0.33357
1.7	-0.73526	-0.30599	-0.66802	-0.33208	-0.59036	-0.35281	-0.50405	-0.36860	-0.41072	-0.37984
1.8	-0.56070	-0.36824	-0.47655	-0.38540	-0.38399	-0.39754	-0.28473	-0.40508	-0.18036	-0.40842
1.9	-0.35884	-0.41257	-0.26240	-0.42040	-0.15975	-0.42382	-0.05245	-0.42323	0.05806	-0.41898
2.0	-0.13967	-0.43703	-0.03598	-0.43581	0.07172	-0.43104	0.18217	-0.42301	0.29415	-0.41203
2.1	0.08617	-0.44062	0.19210	-0.43133	0.30013	-0.41946	0.40930	-0.40523	0.51872	-0.38881
2.2	0.30796	-0.42338	0.41162	-0.40752	0.51589	-0.39015	0.62022	-0.37131	0.72403	-0.35108
2.3	0.51533	-0.38629	0.61307	-0.36580	0.71052	-0.34481	0.80754	-0.32323	0.90392	-0.30099
2.4	0.69876	-0.33124	0.78808	-0.30827	0.87686	-0.28571	0.96538	-0.26332	1.05380	-0.24086
2.5	0.84996	-0.26090	0.92962	-0.23764	1.00917	-0.21551	1.08930	-0.19413	1.17052	-0.17314
2.6	0.96221	-0.17860	1.03227	-0.15706	1.10328	-0.13714	1.17632	-0.11835	1.25225	-0.10021
2.7	1.03065	-0.08816	1.09230	-0.06998	1.15653	-0.05368	1.22479	-0.03867	1.29827	-0.02441
2.8	1.05238	0.00626	1.10773	0.01995	1.16776	0.03174	1.23425	0.04224	1.30879	0.05206
2.9	1.02663	0.10042	1.07836	0.10909	1.13722	0.11603	1.20534	0.12185	1.28474	0.12718
3.0	0.95471	0.19006	1.00568	0.19385	1.06647	0.19623	1.13964	0.19775	1.22768	0.19904
3.1	0.83992	0.27116	0.89279	0.27086	0.95834	0.26955	1.03958	0.26770	1.13966	0.26588
π	0.78074	0.30151	0.83504	0.29987	0.90325	0.29741	0.98859	0.29454	1.09451	0.29182

	ε=5.0	k=0.6	ε=5.0	k=0.7	ε=5.0	k=0.8	ε=5.0	k=0.9	ε=5.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.96027	0.09867	0.95781	0.09859	0.95535	0.09850	0.95289	0.09842	0.95043	0.09834
0.2	0.84440	0.18952	0.83497	0.18888	0.82557	0.18824	0.81621	0.18760	0.80688	0.18696
0.3	0.66193	0.26544	0.64224	0.26338	0.62271	0.26133	0.60333	0.25929	0.58410	0.25725
0.4	0.42772	0.32063	0.39628	0.31607	0.36529	0.31155	0.33474	0.30707	0.30463	0.30262
0.5	0.16047	0.35110	0.11790	0.34296	0.07631	0.33493	0.03570	0.32700	-0.00393	0.31917
0.6	-0.11900	0.35496	-0.17001	0.34237	-0.21920	0.33002	-0.26660	0.31790	-0.31226	0.30600
0.7	-0.38971	0.33245	-0.44469	0.31495	-0.49676	0.29791	-0.54599	0.28130	-0.59246	0.26512
0.8	-0.63226	0.28583	-0.68556	0.26350	-0.73467	0.24193	-0.77974	0.22110	-0.82092	0.20099
0.9	-0.83034	0.21905	-0.87585	0.19255	-0.91587	0.16721	-0.95062	0.14300	-0.98036	0.11989
1.0	-0.97182	0.13729	-1.00376	0.10783	-1.02902	0.08001	-1.04796	0.05378	-1.06095	0.02908
1.1	-1.04937	0.04645	-1.06296	0.01568	-1.06898	-0.01292	-1.06796	-0.03946	-1.06041	-0.06401
1.2	-1.06056	-0.04735	-1.05254	-0.07753	-1.03653	-0.10504	-1.01321	-0.13001	-0.98326	-0.15255
1.3	-1.00751	-0.13834	-0.97648	-0.16598	-0.93755	-0.19048	-0.89160	-0.21204	-0.83943	-0.23082
1.4	-0.89625	-0.22138	-0.84268	-0.24466	-0.78188	-0.26447	-0.71486	-0.28105	-0.64256	-0.29461
1.5	-0.73578	-0.29232	-0.66188	-0.30976	-0.58190	-0.32356	-0.49696	-0.33398	-0.40810	-0.34129
1.6	-0.53702	-0.34813	-0.44637	-0.35871	-0.35116	-0.36566	-0.25258	-0.36926	-0.15170	-0.36981
1.7	-0.31189	-0.38692	-0.20896	-0.39019	-0.10319	-0.39000	0.00423	-0.38666	0.11225	-0.38047
1.8	-0.07231	-0.40794	0.03805	-0.40400	0.14955	-0.39692	0.26105	-0.38703	0.37156	-0.37462
1.9	0.17049	-0.41142	0.28362	-0.40088	0.39636	-0.38765	0.50770	-0.37205	0.61675	-0.35433
2.0	0.40656	-0.39836	0.51838	-0.38229	0.62867	-0.36405	0.73658	-0.34389	0.84131	-0.32205
2.1	0.62753	-0.37040	0.73494	-0.35017	0.84020	-0.32831	0.94261	-0.30498	1.04152	-0.28036
2.2	0.82678	-0.32954	0.92790	-0.30674	1.02685	-0.28278	1.12309	-0.25774	1.21611	-0.23171
2.3	0.99941	-0.27802	1.09371	-0.25429	1.18646	-0.22976	1.27729	-0.20441	1.36580	-0.17825
2.4	1.14217	-0.21816	1.23044	-0.19503	1.31846	-0.17134	1.40601	-0.14696	1.49277	-0.12179
2.5	1.25319	-0.15220	1.33749	-0.13104	1.42350	-0.10939	1.51114	-0.08701	1.60019	-0.06369
2.6	1.33173	-0.08228	1.41523	-0.06415	1.50302	-0.04545	1.59521	-0.02581	1.69171	-0.00493
2.7	1.37798	-0.01034	1.46468	0.00403	1.55892	0.01922	1.66102	0.03569	1.77111	0.05388
2.8	1.39274	0.06184	1.48725	0.07216	1.59323	0.08365	1.71134	0.09689	1.84197	0.11246
2.9	1.37725	0.13269	1.48451	0.13908	1.60791	0.14706	1.74861	0.15737	1.90746	0.17073
3.0	1.33303	0.20079	1.45797	0.20378	1.60461	0.20884	1.77480	0.21686	1.97012	0.22876
3.1	1.26176	0.26482	1.40904	0.26536	1.58458	0.26845	1.79125	0.27516	2.03172	0.28667
π	1.22457	0.28996	1.38239	0.28985	1.57152	0.29248	1.79541	0.29902	2.05727	0.31074

t	ε=6.0 g(t)	k=0.1 h(t)	ε=6.0 g(t)	k=0.2 h(t)	ε=6.0 g(t)	k=0.3 h(t)	ε=6.0 g(t)	k=0.4 h(t)	ε=6.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.96718	0.09890	0.96422	0.09880	0.96126	0.09870	0.95830	0.09860	0.95534	0.09850
0.2	0.87091	0.19132	0.85949	0.19054	0.84812	0.18977	0.83679	0.18900	0.82551	0.18823
0.3	0.71758	0.27120	0.69346	0.26870	0.66956	0.26621	0.64589	0.26373	0.62243	0.26127
0.4	0.51734	0.33336	0.47816	0.32777	0.43964	0.32224	0.40177	0.31676	0.36455	0.31133
0.5	0.28338	0.37379	0.22908	0.36370	0.17625	0.35376	0.12486	0.34396	0.07491	0.33432
0.6	0.03101	0.38997	-0.03609	0.37413	-0.10045	0.35864	-0.16214	0.34348	-0.22121	0.32866
0.7	-0.22336	0.38095	-0.29878	0.35856	-0.36974	0.33684	-0.43637	0.31577	-0.49880	0.29535
0.8	-0.46341	0.34746	-0.54096	0.31833	-0.61197	0.29035	-0.67670	0.26348	-0.73541	0.23769
0.9	-0.67390	0.29179	-0.74640	0.25646	-0.81013	0.22291	-0.86552	0.19108	-0.91300	0.16092
1.0	-0.84168	0.21762	-0.90181	0.17737	-0.95100	0.13968	-0.98995	0.10443	-1.01932	0.07154
1.1	-0.95653	0.12975	-0.99762	0.08654	-1.02599	0.04674	-1.04263	0.01018	-1.04850	-0.02329
1.2	-1.01171	0.03377	-1.02851	-0.00992	-1.03145	-0.04937	-1.02185	-0.08481	-1.00098	-0.11646
1.3	-1.00431	-0.06429	-0.99358	-0.10578	-0.96867	-0.14226	-0.93126	-0.17405	-0.88291	-0.20147
1.4	-0.93535	-0.15843	-0.89616	-0.19501	-0.84343	-0.22600	-0.77912	-0.25183	-0.70505	-0.27289
1.5	-0.80960	-0.24296	-0.74341	-0.27222	-0.66523	-0.29559	-0.57725	-0.31359	-0.48144	-0.32668
1.6	-0.63517	-0.31290	-0.54561	-0.33296	-0.44642	-0.34718	-0.33986	-0.35612	-0.22796	-0.36030
1.7	-0.42290	-0.36421	-0.31539	-0.37394	-0.20113	-0.37823	-0.08231	-0.37765	0.03905	-0.37273
1.8	-0.18568	-0.39406	-0.06678	-0.39319	0.05578	-0.38758	0.17999	-0.37779	0.30402	-0.36433
1.9	0.06238	-0.40092	0.18569	-0.39002	0.30967	-0.37535	0.43263	-0.35741	0.55302	-0.33664
2.0	0.30687	-0.38461	0.42783	-0.36505	0.54693	-0.34287	0.66285	-0.31843	0.77442	-0.29206
2.1	0.53382	-0.34629	0.64659	-0.32010	0.75565	-0.29246	0.86013	-0.26357	0.95926	-0.23363
2.2	0.73052	-0.28832	0.83066	-0.25794	0.92610	-0.22719	1.01644	-0.19611	1.10129	-0.16473
2.3	0.88617	-0.21415	0.97097	-0.18217	1.05101	-0.15073	1.12634	-0.11967	1.19688	-0.08885
2.4	0.99244	-0.12803	1.06102	-0.09696	1.12568	-0.06705	1.18685	-0.03795	1.24474	-0.00939
2.5	1.04380	-0.03483	1.09702	-0.00680	1.14795	0.01977	1.19732	0.04538	1.24560	0.07044
2.6	1.03782	0.06029	1.07797	0.08371	1.11809	0.10571	1.15912	0.12689	1.20175	0.14777
2.7	0.97515	0.15213	1.00554	0.17008	1.03854	0.18697	1.07533	0.20343	1.11677	0.22007
2.8	0.85950	0.23571	0.88387	0.24814	0.91372	0.26010	0.95045	0.27222	0.99513	0.28516
2.9	0.69734	0.30653	0.71931	0.31417	0.74969	0.32207	0.79010	0.33086	0.84194	0.34123
3.0	0.49757	0.36083	0.52010	0.36507	0.55387	0.37039	0.60082	0.37739	0.66279	0.38679
3.1	0.27101	0.39570	0.29596	0.39847	0.33475	0.40314	0.38980	0.41028	0.46358	0.42065
π	0.17175	0.40401	0.19792	0.40681	0.23893	0.41184	0.29745	0.41967	0.37626	0.43107

	ε=6.0	k=0.6	ε=6.0	k=0.7	ε=6.0	k=0.8	ε=6.0	k=0.9	ε=6.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.95239	0.09840	0.94944	0.09830	0.94650	0.09821	0.94356	0.09811	0.94062	0.09801
0.2	0.81428	0.18747	0.80309	0.18670	0.79195	0.18594	0.78085	0.18518	0.76980	0.18442
0.3	0.59920	0.25882	0.57618	0.25639	0.55338	0.25397	0.53079	0.25156	0.50842	0.24916
0.4	0.32797	0.30596	0.29202	0.30064	0.25670	0.29537	0.22200	0.29015	0.18791	0.28498
0.5	0.02637	0.32482	-0.02078	0.31548	-0.06658	0.30627	-0.11103	0.29721	-0.15416	0.28829
0.6	-0.27771	0.31418	-0.33170	0.30001	-0.38325	0.28617	-0.43240	0.27264	-0.47921	0.25943
0.7	-0.55717	0.27555	-0.61160	0.25637	-0.66220	0.23779	-0.70911	0.21981	-0.75244	0.20240
0.8	-0.78833	0.21297	-0.83571	0.18927	-0.87778	0.16658	-0.91476	0.14486	-0.94687	0.12409
0.9	-0.95299	0.13237	-0.98589	0.10537	-1.01209	0.07988	-1.03196	0.05584	-1.04586	0.03319
1.0	-1.03976	0.04090	-1.05187	0.01241	-1.05624	-0.01399	-1.05343	-0.03843	-1.04398	-0.06097
1.1	-1.04450	-0.05384	-1.03150	-0.08161	-1.01029	-0.10674	-0.98166	-0.12937	-0.94635	-0.14962
1.2	-0.97002	-0.14455	-0.93009	-0.16929	-0.88224	-0.19088	-0.82746	-0.20952	-0.76667	-0.22540
1.3	-0.82506	-0.22482	-0.75909	-0.24437	-0.68625	-0.26041	-0.60771	-0.27319	-0.52457	-0.28295
1.4	-0.62289	-0.28956	-0.53420	-0.30220	-0.44040	-0.31113	-0.34279	-0.31668	-0.24257	-0.31916
1.5	-0.37964	-0.33530	-0.27349	-0.33988	-0.16450	-0.34080	-0.05405	-0.33843	0.05662	-0.33312
1.6	-0.11260	-0.36020	0.00454	-0.35629	0.12198	-0.34899	0.23837	-0.33871	0.35251	-0.32580
1.7	0.16119	-0.36398	0.28250	-0.35187	0.40159	-0.33683	0.51720	-0.31928	0.62825	-0.29958
1.8	0.42627	-0.34769	0.54531	-0.32831	0.65989	-0.30661	0.76895	-0.28296	0.87155	-0.25773
1.9	0.66950	-0.31345	0.78087	-0.28824	0.88612	-0.26136	0.98434	-0.23315	1.07480	-0.20393
2.0	0.88060	-0.26408	0.98047	-0.23480	1.07321	-0.20449	1.15816	-0.17341	1.23471	-0.14182
2.1	1.05230	-0.20283	1.13861	-0.17137	1.21760	-0.13941	1.28875	-0.10713	1.35159	-0.07469
2.2	1.18021	-0.13310	1.25279	-0.10127	1.31863	-0.06931	1.37732	-0.03729	1.42847	-0.00528
2.3	1.26248	-0.05817	1.32293	-0.02756	1.37792	0.00303	1.42712	0.03364	1.47016	0.06430
2.4	1.29943	0.01887	1.35081	0.04705	1.39868	0.07533	1.44269	0.10383	1.48243	0.13265
2.5	1.29300	0.09532	1.33955	0.12035	1.38505	0.14582	1.42915	0.17195	1.47131	0.19896
2.6	1.24635	0.16883	1.29301	0.19051	1.34158	0.21320	1.39166	0.23724	1.44259	0.26292
2.7	1.16340	0.23745	1.21543	0.25612	1.27277	0.27658	1.33500	0.29929	1.40141	0.32466
2.8	1.04852	0.29958	1.11106	0.31611	1.18278	0.33539	1.26336	0.35800	1.35209	0.38453
2.9	0.90631	0.35391	0.98396	0.36966	1.07526	0.38925	1.18012	0.41346	1.29800	0.44308
3.0	0.74140	0.39939	0.83793	0.41611	0.95330	0.43790	1.08789	0.46579	1.24157	0.50084
3.1	0.55842	0.43516	0.67645	0.45489	0.81939	0.48107	0.98849	0.51505	1.18433	0.55829
π	0.47810	0.44700	0.60550	0.46864	0.76068	0.49735	0.94533	0.53463	1.16047	0.58216

t	ε=7.0 g(t)	k=0.1 h(t)	ε=7.0 g(t)	k=0.2 h(t)	ε=7.0 g(t)	k=0.3 h(t)	ε=7.0 g(t)	k=0.4 h(t)	ε=7.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.96174	0.09872	0.95829	0.09860	0.95485	0.09849	0.95141	0.09837	0.94797	0.09826
0.2	0.84995	0.18989	0.83673	0.18899	0.82358	0.18810	0.81048	0.18720	0.79745	0.18631
0.3	0.67325	0.26657	0.64561	0.26368	0.61827	0.26081	0.59123	0.25795	0.56448	0.25512
0.4	0.44524	0.32293	0.40102	0.31654	0.35768	0.31021	0.31521	0.30396	0.27360	0.29778
0.5	0.18340	0.35477	0.12340	0.34334	0.06534	0.33212	0.00919	0.32110	-0.04508	0.31027
0.6	-0.09232	0.35977	-0.16433	0.34210	-0.23277	0.32488	-0.29775	0.30811	-0.35935	0.29178
0.7	-0.36111	0.33768	-0.43879	0.31314	-0.51078	0.28947	-0.57728	0.26665	-0.63849	0.24466
0.8	-0.60289	0.29030	-0.67812	0.25908	-0.74520	0.22932	-0.80452	0.20099	-0.85644	0.17405
0.9	-0.79982	0.22132	-0.86377	0.18448	-0.91706	0.14989	-0.96036	0.11746	-0.99428	0.08710
1.0	-0.93767	0.13597	-0.98191	0.09544	-1.01333	0.05807	-1.03292	0.02370	-1.04165	-0.00780
1.1	-1.00678	0.04065	-1.02441	-0.00096	-1.02775	-0.03846	-1.01821	-0.07209	-0.99710	-0.10208
1.2	-1.00267	-0.05763	-0.98919	-0.09732	-0.96097	-0.13200	-0.91984	-0.16201	-0.86751	-0.18770
1.3	-0.92626	-0.15178	-0.88016	-0.18643	-0.82007	-0.21538	-0.74820	-0.23907	-0.66659	-0.25795
1.4	-0.78367	-0.23509	-0.70660	-0.26190	-0.61749	-0.28260	-0.51887	-0.29776	-0.41300	-0.30790
1.5	-0.58560	-0.30177	-0.48215	-0.31855	-0.36969	-0.32918	-0.25087	-0.33434	-0.12809	-0.33463
1.6	-0.34646	-0.34731	-0.22362	-0.35272	-0.09552	-0.35238	0.03520	-0.34701	0.16621	-0.33727
1.7	-0.08326	-0.36876	0.05043	-0.36250	0.18533	-0.35128	0.31898	-0.33582	0.44929	-0.31677
1.8	0.18569	-0.36487	0.32114	-0.34769	0.45388	-0.32666	0.58184	-0.30244	0.70326	-0.27562
1.9	0.44205	-0.33616	0.57056	-0.30977	0.69306	-0.28084	0.80797	-0.24989	0.91394	-0.21743
2.0	0.66866	-0.28480	0.78289	-0.25167	0.88874	-0.21734	0.98518	-0.18217	1.07135	-0.14650
2.1	0.85068	-0.21443	0.94527	-0.17749	1.03030	-0.14056	1.10525	-0.10383	1.16970	-0.06744
2.2	0.97648	-0.12989	1.04850	-0.09218	1.11098	-0.05546	1.16388	-0.01968	1.20711	-0.01516
2.3	1.03831	-0.03682	1.08731	-0.00119	1.12794	0.03285	1.16050	-0.06554	1.18512	0.09706
2.4	1.03267	0.05864	1.06048	0.08988	1.08200	0.11935	1.09775	0.14744	1.10798	0.17649
2.5	0.96042	0.15033	0.97065	0.17564	0.97731	0.19940	0.98099	0.22212	0.98200	0.24425
2.6	0.82665	0.23240	0.82402	0.25113	0.82084	0.26890	0.81771	0.28629	0.81487	0.30382
2.7	0.64024	0.29968	0.62980	0.31213	0.62182	0.32449	0.61688	0.33735	0.61518	0.35129
2.8	0.41327	0.34798	0.39961	0.35532	0.39117	0.36359	0.38854	0.37339	0.39190	0.38532
2.9	0.16022	0.37433	0.14680	0.37841	0.14093	0.38451	0.14326	0.39320	0.15410	0.40512
3.0	-0.10290	0.37715	-0.11426	0.38026	-0.11629	0.38643	-0.10815	0.39623	-0.08927	0.41037
3.1	-0.35958	0.35630	-0.36896	0.36086	-0.36783	0.36940	-0.35502	0.38255	-0.32951	0.40115
π	-0.46059	0.34092	-0.46961	0.34676	-0.46784	0.35693	-0.45390	0.37208	-0.42652	0.39314

	ε=7.0	k=0.6	ε=7.0	k=0.7	ε=7.0	k=0.8	ε=7.0	k=0.9	ε=7.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.94453	0.09814	0.94110	0.09802	0.93768	0.09791	0.93425	0.09779	0.93084	0.09768
0.2	0.78448	0.18542	0.77158	0.18454	0.75873	0.18366	0.74595	0.18278	0.73323	0.18190
0.3	0.53803	0.25230	0.51188	0.24951	0.48601	0.24673	0.46043	0.24396	0.43514	0.24122
0.4	0.23283	0.29167	0.19291	0.28563	0.15381	0.27966	0.11554	0.27376	0.07808	0.26793
0.5	-0.09752	0.29964	-0.14815	0.28920	-0.19700	0.27895	-0.24411	0.26890	-0.28952	0.25902
0.6	-0.41766	0.27588	-0.47276	0.26040	-0.52475	0.24534	-0.57370	0.23069	-0.61970	0.21645
0.7	-0.69460	0.22349	-0.74580	0.20310	-0.79227	0.18348	-0.83420	0.16461	-0.87176	0.14648
0.8	-0.90133	0.14844	-0.93954	0.12413	-0.97140	0.10107	-0.99726	0.07922	-1.01741	0.05854
0.9	-1.01944	0.05874	-1.03639	0.03229	-1.04571	0.00768	-1.04792	-0.01517	-1.04353	-0.03634
1.0	-1.04041	-0.03659	-1.03006	-0.06280	-1.01142	-0.08656	-0.98526	-0.10800	-0.95232	-0.12723
1.1	-0.96569	-0.12863	-0.92513	-0.15197	-0.87652	-0.17228	-0.82090	-0.18977	-0.75924	-0.20461
1.2	-0.80555	-0.20936	-0.73543	-0.22729	-0.65854	-0.24177	-0.57611	-0.25306	-0.48933	-0.26143
1.3	-0.57713	-0.27241	-0.48152	-0.28286	-0.38133	-0.28964	-0.27800	-0.29310	-0.17281	-0.29356
1.4	-0.30198	-0.31353	-0.18764	-0.31512	-0.07169	-0.31310	0.04439	-0.30789	0.15928	-0.29987
1.5	-0.00348	-0.33064	0.12106	-0.32288	0.24390	-0.31185	0.36357	-0.29801	0.47880	-0.28178
1.6	0.29546	-0.32377	0.42119	-0.30706	0.54185	-0.28766	0.65616	-0.26605	0.76301	-0.24267
1.7	0.57442	-0.29474	0.69283	-0.27028	0.80323	-0.24389	0.90456	-0.21602	0.99597	-0.18710
1.8	0.81665	-0.24676	0.92078	-0.21634	1.01466	-0.18483	1.09750	-0.15263	1.16872	-0.12011
1.9	1.00990	-0.18389	1.09495	-0.14968	1.16841	-0.11518	1.22977	-0.08071	1.27868	-0.04657
2.0	1.14653	-0.11064	1.21018	-0.07489	1.26187	-0.03950	1.30132	-0.00472	1.32838	0.02924
2.1	1.22326	-0.03156	1.26566	0.00365	1.29666	0.03806	1.31612	0.07154	1.32395	0.10395
2.2	1.24055	0.04909	1.26406	0.08208	1.27751	0.11412	1.28077	0.14515	1.27371	0.17514
2.3	1.20184	0.12758	1.21056	0.15720	1.21112	0.18601	1.20327	0.21405	1.18675	0.24135
2.4	1.11275	0.20077	1.11191	0.22651	1.10514	0.25188	1.09202	0.27701	1.07199	0.30198
2.5	0.98029	0.26617	0.97560	0.28819	0.96741	0.31057	0.95498	0.33349	0.93741	0.35710
2.6	0.81221	0.32195	0.80927	0.34106	0.80528	0.36148	0.79918	0.38349	0.78965	0.40728
2.7	0.61650	0.36681	0.62022	0.38440	0.62535	0.40446	0.63048	0.42736	0.63386	0.45339
2.8	0.40101	0.39996	0.41518	0.41788	0.43322	0.43962	0.45345	0.46567	0.47370	0.49645
2.9	0.17329	0.42096	0.20018	0.44142	0.23355	0.46720	0.27152	0.49901	0.31154	0.53750
3.0	-0.05953	0.42968	-0.01933	0.45506	0.03019	0.48748	0.08714	0.52793	0.14868	0.57743
3.1	-0.29068	0.42621	-0.23847	0.45893	-0.17364	0.50064	-0.09793	0.55280	-0.01431	0.61694
π	-0.38479	0.42122	-0.32840	0.45768	-0.25786	0.50405	-0.17478	0.56200	-0.08211	0.63334

t	ε=8.0 g(t)	k=0.1 h(t)	ε=8.0 g(t)	k=0.2 h(t)	ε=8.0 g(t)	k=0.3 h(t)	ε=8.0 g(t)	k=0.4 h(t)	ε=8.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.95632	0.09853	0.95238	0.09840	0.94845	0.09827	0.94453	0.09814	0.94061	0.09801
0.2	0.82915	0.18847	0.81416	0.18745	0.79925	0.18643	0.78442	0.18542	0.76968	0.18441
0.3	0.62967	0.26198	0.59865	0.25871	0.56802	0.25547	0.53777	0.25225	0.50791	0.24905
0.4	0.37541	0.31269	0.32654	0.30552	0.27880	0.29845	0.23217	0.29146	0.18664	0.28457
0.5	0.08857	0.33629	0.02371	0.32363	-0.03868	0.31122	-0.09867	0.29907	-0.15630	0.28716
0.6	-0.20593	0.33084	-0.28136	0.31154	-0.35235	0.29281	-0.41903	0.27464	-0.48154	0.25703
0.7	-0.48275	0.29694	-0.56046	0.27063	-0.63122	0.24541	-0.69530	0.22124	-0.75299	0.19811
0.8	-0.71832	0.23765	-0.78832	0.20495	-0.84858	0.17406	-0.89965	0.14491	-0.94205	0.11745
0.9	-0.89288	0.15816	-0.94491	0.12072	-0.98459	0.08599	-1.01284	0.05386	-1.03049	0.02420
1.0	-0.99213	0.06532	-1.01717	0.02569	-1.02792	-0.01020	-1.02573	-0.04257	-1.01185	-0.07161
1.1	-1.00831	-0.03299	-0.99995	-0.07161	-0.97644	-0.10549	-0.93963	-0.13493	-0.89124	-0.16026
1.2	-0.94075	-0.12859	-0.89610	-0.16274	-0.83685	-0.19129	-0.76537	-0.21469	-0.68378	-0.23337
1.3	-0.79574	-0.21360	-0.71588	-0.24003	-0.62353	-0.26029	-0.52143	-0.27497	-0.41208	-0.28463
1.4	-0.58578	-0.28121	-0.47569	-0.29725	-0.35661	-0.30697	-0.23153	-0.31110	-0.10310	-0.31031
1.5	-0.32846	-0.32609	-0.19636	-0.33006	-0.05981	-0.32804	0.07816	-0.32085	0.21497	-0.30925
1.6	-0.04481	-0.34489	0.09886	-0.33628	0.24195	-0.32249	0.38164	-0.30439	0.51556	-0.28274
1.7	0.24241	-0.33635	0.38636	-0.31592	0.52478	-0.29154	0.65530	-0.26400	0.77597	-0.23407
1.8	0.51067	-0.30143	0.64401	-0.27110	0.76761	-0.23828	0.87968	-0.20368	0.97881	-0.16794
1.9	0.73928	-0.24308	0.85286	-0.20569	0.95365	-0.16735	1.04056	-0.12860	1.11275	-0.08993
2.0	0.91106	-0.16603	0.99832	-0.12496	1.07127	-0.08437	1.12944	-0.04457	1.17253	-0.00587
2.1	1.01345	-0.07632	1.07103	-0.03510	1.11434	0.00451	1.14343	0.04244	1.15841	0.07861
2.2	1.03934	0.01915	1.06718	0.05728	1.08218	0.09312	1.08478	0.12681	1.07531	0.15846
2.3	0.98747	0.11321	0.98842	0.14567	0.97909	0.17566	0.96005	0.20350	0.93169	0.22944
2.4	0.86230	0.19888	0.84149	0.22403	0.81360	0.24703	0.77919	0.26833	0.73849	0.28827
2.5	0.67361	0.26991	0.63745	0.28721	0.59763	0.30311	0.55450	0.31811	0.50804	0.33261
2.6	0.43563	0.32119	0.39078	0.33121	0.34545	0.34086	0.29971	0.35068	0.25321	0.36107
2.7	0.16596	0.34913	0.11832	0.35337	0.07279	0.35846	0.02915	0.36489	-0.01325	0.37309
2.8	-0.11572	0.35182	-0.16189	0.35250	-0.20414	0.35525	-0.24294	0.36053	-0.27921	0.36879
2.9	-0.38908	0.32916	-0.43167	0.32886	-0.46945	0.33175	-0.50303	0.33827	-0.53344	0.34894
3.0	-0.63452	0.28287	-0.67378	0.28412	-0.70829	0.28951	-0.73862	0.29951	-0.76582	0.31475
3.1	-0.83449	0.21630	-0.87290	0.22125	-0.90748	0.23106	-0.93866	0.24633	-0.96736	0.26788
π	-0.90076	0.18374	-0.93995	0.19069	-0.97596	0.20274	-1.00911	0.22057	-1.04021	0.24511

	ε=8.0	k=0.6	ε=8.0	k=0.7	ε=8.0	k=0.8	ε=8.0	k=0.9	ε=8.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.93669	0.09788	0.93278	0.09775	0.92888	0.09761	0.92498	0.09748	0.92109	0.09735
0.2	0.75502	0.18340	0.74043	0.18239	0.72593	0.18139	0.71150	0.18039	0.69716	0.17939
0.3	0.47842	0.24588	0.44931	0.24273	0.42057	0.23960	0.39221	0.23650	0.36421	0.23342
0.4	0.14218	0.27776	0.09830	0.27105	0.05647	0.26442	0.01518	0.25789	-0.02508	0.25144
0.5	-0.21162	0.27551	-0.26469	0.26410	-0.31553	0.25294	-0.36422	0.24201	-0.41078	0.23132
0.6	-0.54000	0.23996	-0.59453	0.22342	-0.64526	0.20740	-0.69231	0.19188	-0.73580	0.17687
0.7	-0.80457	0.17597	-0.85030	0.15481	-0.89043	0.13459	-0.92522	0.11528	-0.95492	0.09687
0.8	-0.97629	0.09161	-1.00284	0.06733	-1.02217	0.04455	-1.03474	0.02322	-1.04097	0.00328
0.9	-1.03839	-0.00308	-1.03730	-0.02813	-1.02797	-0.05103	-1.01110	-0.07190	-0.98738	-0.09083
1.0	-0.98750	-0.09751	-0.95381	-0.12047	-0.91184	-0.14066	-0.86259	-0.15824	-0.80699	-0.17339
1.1	-0.83288	-0.18176	-0.76604	-0.19972	-0.69210	-0.21440	-0.61234	-0.22604	-0.52794	-0.23489
1.2	-0.59408	-0.24773	-0.49806	-0.25817	-0.39735	-0.26503	-0.29346	-0.26867	-0.18774	-0.26940
1.3	-0.29770	-0.28981	-0.18029	-0.29098	-0.06165	-0.28860	0.05663	-0.28309	0.17316	-0.27485
1.4	0.02634	-0.30521	0.15475	-0.29639	0.28034	-0.28437	0.40157	-0.26964	0.51712	-0.25265
1.5	0.34834	-0.29391	0.47636	-0.27546	0.59740	-0.25448	0.71014	-0.23149	0.81349	-0.20695
1.6	0.64174	-0.25825	0.75853	-0.23154	0.84464	-0.20319	0.95905	-0.17372	1.04102	-0.14357
1.7	0.88524	-0.20238	0.98188	-0.16954	1.06500	-0.13606	1.13399	-0.10242	1.18847	-0.06903
1.8	1.06396	-0.13162	1.13436	-0.09521	1.18957	-0.05915	1.22935	-0.02384	1.25370	0.01040
1.9	1.16969	-0.05175	1.21105	-0.01443	1.23677	0.02168	1.24693	0.05634	1.24183	0.08930
2.0	1.20043	0.03149	1.21318	0.06729	1.21096	0.10133	1.19408	0.13344	1.16296	0.16348
2.1	1.15946	0.11291	1.14681	0.14529	1.12074	0.17567	1.08160	0.20396	1.02981	0.23013
2.2	1.05405	0.18813	1.02126	0.21587	0.97714	0.24172	0.92191	0.26568	0.85579	0.28776
2.3	0.89422	0.25368	0.84771	0.27637	0.79214	0.29761	0.72743	0.31748	0.65344	0.33699
2.4	0.69149	0.30714	0.63794	0.32515	0.57746	0.34246	0.50950	0.35916	0.43345	0.37530
2.5	0.45788	0.34696	0.40340	0.36140	0.34369	0.37611	0.27769	0.39121	0.20414	0.40675
2.6	0.20519	0.37242	0.15457	0.38499	0.09992	0.39899	0.03951	0.41456	-0.02860	0.43174
2.7	-0.05555	0.38344	-0.09930	0.39628	-0.14650	0.41184	-0.19954	0.43031	-0.26118	0.45178
2.8	-0.31436	0.38046	-0.35938	0.39594	-0.38979	0.41558	-0.43573	0.43964	-0.49188	0.46834
2.9	-0.56233	0.36428	-0.59199	0.38483	-0.62549	0.41112	-0.66670	0.44361	-0.72037	0.48272
3.0	-0.79160	0.33594	-0.81850	0.36385	-0.85006	0.39930	-0.89097	0.44308	-0.94721	0.49594
3.1	-0.99522	0.29670	-1.02493	0.33394	-1.06049	0.38083	-1.10747	0.43867	-1.17328	0.50874
π	-1.07086	0.27750	-1.10379	0.31908	-1.14316	0.37132	-1.19495	0.43577	-1.26725	0.51403

t	ε=9.0 g(t)	k=0.1 h(t)	ε=9.0 g(t)	k=0.2 h(t)	ε=9.0 g(t)	k=0.3 h(t)	ε=9.0 g(t)	k=0.4 h(t)	ε=9.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.95091	0.09835	0.94649	0.09821	0.94207	0.09806	0.93767	0.09791	0.93327	0.09776
0.2	0.80850	0.18706	0.79177	0.18592	0.77514	0.18478	0.75861	0.18364	0.74219	0.18251
0.3	0.58685	0.25744	0.55258	0.25380	0.51880	0.25019	0.48550	0.24662	0.45268	0.24307
0.4	0.30780	0.30264	0.25467	0.29473	0.20294	0.28693	0.15258	0.27925	0.10358	0.27168
0.5	-0.00125	0.31835	-0.07016	0.30453	-0.13606	0.29103	-0.19901	0.27785	-0.25909	0.26498
0.6	-0.31022	0.30314	-0.38770	0.28240	-0.45981	0.26236	-0.52676	0.24301	-0.58871	0.22433
0.7	-0.58923	0.25864	-0.66499	0.23091	-0.73251	0.20449	-0.79219	0.17936	-0.84441	0.15545
0.8	-0.81165	0.18928	-0.87392	0.15566	-0.92497	0.12419	-0.96552	0.09480	-0.99625	0.06739
0.9	-0.95657	0.10183	-0.99405	0.06456	-1.01776	0.03047	-1.02887	-0.00059	-1.02850	-0.02880
1.0	-1.01080	0.00471	-1.01447	-0.03302	-1.00286	-0.06650	-0.97769	-0.09596	-0.94060	-0.12170
1.1	-0.96989	-0.09285	-0.93455	-0.12738	-0.88396	-0.15662	-0.82044	-0.18098	-0.74613	-0.20087
1.2	-0.83844	-0.18176	-0.76361	-0.20935	-0.67540	-0.23089	-0.57663	-0.24697	-0.46988	-0.25812
1.3	-0.62938	-0.25387	-0.51951	-0.27131	-0.39990	-0.28235	-0.27376	-0.28775	-0.14392	-0.28820
1.4	-0.36258	-0.30271	-0.22646	-0.30775	-0.08571	-0.30660	0.05634	-0.30012	0.19683	-0.28914
1.5	-0.06279	-0.32406	0.08759	-0.31578	0.23666	-0.30207	0.38126	-0.28388	0.51878	-0.26206
1.6	0.24267	-0.31625	0.39380	-0.29521	0.53759	-0.26999	0.67136	-0.24155	0.79298	-0.21072
1.7	0.52658	-0.28028	0.66513	-0.24841	0.79104	-0.21398	0.90234	-0.17785	0.99756	-0.14079
1.8	0.76409	-0.21958	0.87863	-0.17997	0.97664	-0.13956	1.05695	-0.09904	1.11884	-0.05903
1.9	0.93491	-0.13970	1.01724	-0.09616	1.08096	-0.05348	1.12573	-0.01213	1.15151	0.02746
2.0	1.02492	-0.04772	1.07083	-0.00428	1.09804	0.03695	1.10687	0.07576	1.09781	0.11197
2.1	1.02716	0.04834	1.03667	0.08797	1.02918	0.12453	1.00544	0.15806	0.96625	0.18859
2.2	0.94223	0.14032	0.91917	0.17318	0.88217	0.20269	0.83215	0.22911	0.76992	0.25264
2.3	0.77804	0.22052	0.72912	0.24474	0.67010	0.26595	0.60180	0.28452	0.52479	0.30075
2.4	0.54892	0.28232	0.46250	0.29735	0.40998	0.31018	0.33182	0.32126	0.24816	0.33092
2.5	0.27432	0.32075	0.19904	0.32729	0.12120	0.33280	0.04078	0.33772	-0.04264	0.34238
2.6	-0.02284	0.33281	-0.09942	0.33265	-0.17589	0.33280	-0.25285	0.33363	-0.33129	0.33544
2.7	-0.31817	0.31767	-0.39056	0.31335	-0.46136	0.31066	-0.53168	0.30989	-0.60307	0.31130
2.8	-0.58765	0.27670	-0.65311	0.27108	-0.71678	0.26824	-0.78016	0.26841	-0.84529	0.27185
2.9	-0.80956	0.21332	-0.86827	0.20913	-0.92619	0.20858	-0.98513	0.21194	-1.04746	0.21949
3.0	-0.96612	0.13271	-1.02083	0.13209	-1.07683	0.13568	-1.13611	0.14383	-1.20131	0.15699
3.1	-1.04486	0.04142	-1.10015	0.04556	-1.15969	0.05423	-1.22558	0.06793	-1.30074	0.08736
π	-1.05299	0.00202	-1.11007	0.00825	-1.17287	0.01905	-1.24352	0.03504	-1.32505	0.05706

	ε=9.0	k=0.6	ε=9.0	k=0.7	ε=9.0	k=0.8	ε=9.0	k=0.9	ε=9.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.92888	0.09761	0.92449	0.09747	0.92011	0.09732	0.91574	0.09717	0.91137	0.09702
0.2	0.72587	0.18138	0.70965	0.18026	0.69353	0.17914	0.67751	0.17802	0.66159	0.17691
0.3	0.42033	0.23955	0.38845	0.23606	0.35704	0.23260	0.32609	0.22917	0.29559	0.22577
0.4	0.05591	0.26422	0.00955	0.25688	-0.03550	0.24964	-0.07928	0.24252	-0.12181	0.23550
0.5	-0.31636	0.25241	-0.37089	0.24015	-0.42275	0.22818	-0.47200	0.21650	-0.51870	0.20511
0.6	-0.64583	0.20632	-0.69831	0.18894	-0.74629	0.17219	-0.78996	0.15606	-0.82947	0.14052
0.7	-0.88953	0.13274	-0.92792	0.11119	-0.95992	0.09076	-0.98587	0.07141	-1.00608	0.05310
0.8	-1.01785	0.04188	-1.03094	0.01819	-1.03613	-0.00374	-1.03401	-0.02403	-1.02514	-0.04272
0.9	-1.01771	-0.05431	-0.99751	-0.07727	-0.96884	-0.09782	-0.93260	-0.11608	-0.88963	-0.13220
1.0	-0.89309	-0.14395	-0.83657	-0.16295	-0.77236	-0.17894	-0.70168	-0.19213	-0.62565	-0.20273
1.1	-0.66297	-0.21666	-0.57277	-0.22870	-0.47716	-0.23732	-0.37764	-0.24284	-0.27558	-0.24556
1.2	-0.35746	-0.26485	-0.24145	-0.26765	-0.12369	-0.26694	-0.00584	-0.26315	0.11064	-0.25665
1.3	-0.01288	-0.28432	0.11718	-0.27671	0.24439	-0.26589	0.36712	-0.25237	0.48398	-0.23660
1.4	0.33334	-0.27436	0.46378	-0.25646	0.58644	-0.23604	0.69994	-0.21365	0.80314	-0.18976
1.5	0.64707	-0.23739	0.76439	-0.21057	0.86942	-0.18222	0.96115	-0.15288	1.03889	-0.12304
1.6	0.90082	-0.17828	0.99366	-0.14489	1.07067	-0.11112	1.13139	-0.07749	1.17562	-0.04444
1.7	1.07570	-0.10347	1.13615	-0.06645	1.17869	-0.03024	1.20336	0.00473	1.21049	0.03812
1.8	1.16197	-0.02004	1.18634	0.01747	1.19225	0.05315	1.18024	0.08669	1.15105	0.11784
1.9	1.15853	0.06499	1.14730	0.10017	1.11850	0.13276	1.07301	0.16259	1.01185	0.18953
2.0	1.07150	0.14542	1.02873	0.17601	0.97039	0.20365	0.89747	0.22826	0.81106	0.24981
2.1	0.91240	0.21615	0.84473	0.24075	0.76409	0.26243	0.67137	0.28120	0.56752	0.29710
2.2	0.69619	0.27342	0.61159	0.29159	0.51671	0.30724	0.41214	0.32044	0.29842	0.33123
2.3	0.43944	0.31489	0.34599	0.32710	0.24456	0.33750	0.13520	0.34618	0.01795	0.35316
2.4	0.15888	0.33943	0.06362	0.34697	-0.03809	0.35364	-0.14687	0.35948	-0.26335	0.36450
2.5	-0.12980	0.34701	-0.22171	0.35178	-0.31957	0.35675	-0.42476	0.36193	-0.53873	0.36723
2.6	-0.41259	0.33842	-0.49848	0.34271	-0.59094	0.34835	-0.69221	0.35528	-0.80468	0.36338
2.7	-0.67752	0.31507	-0.75747	0.32132	-0.84575	0.33008	-0.94556	0.34130	-1.06044	0.35483
2.8	-0.91471	0.27876	-0.99158	0.28931	-1.07962	0.30358	-1.18317	0.32158	-1.30713	0.34318
2.9	-1.11627	0.23156	-1.19541	0.24843	-1.28964	0.27034	-1.40464	0.29743	-1.54706	0.32972
3.0	-1.27601	0.17566	-1.36485	0.20038	-1.47377	0.23165	-1.61016	0.26988	-1.78292	0.31536
3.1	-1.38922	0.11335	-1.49661	0.14679	-1.63030	0.18860	-1.79982	0.23966	-2.01709	0.30070
π	-1.42175	0.08609	-1.53965	0.12324	-1.68684	0.16964	-1.87394	0.22642	-2.11437	0.29458

t	ε=10.0 g(t)	k=0.1 h(t)	ε=10.0 g(t)	k=0.2 h(t)	ε=10.0 g(t)	k=0.3 h(t)	ε=10.0 g(t)	k=0.4 h(t)	ε=10.0 g(t)	k=0.5 h(t)
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.94550	0.09817	0.94060	0.09801	0.93571	0.09784	0.93082	0.09768	0.92595	0.09751
0.2	0.78800	0.18566	0.76956	0.18439	0.75124	0.18313	0.73305	0.18187	0.71499	0.18062
0.3	0.54477	0.25294	0.50739	0.24895	0.47061	0.24498	0.43441	0.24106	0.39879	0.23717
0.4	0.24239	0.29278	0.18536	0.28415	0.13003	0.27567	0.07635	0.26732	0.02431	0.25912
0.5	-0.08623	0.30093	-0.15843	0.28604	-0.22702	0.27155	-0.29211	0.25743	-0.35378	0.24369
0.6	-0.40560	0.27664	-0.48385	0.25464	-0.55580	0.23348	-0.62168	0.21315	-0.68175	0.19361
0.7	-0.68148	0.22268	-0.75350	0.19385	-0.81604	0.16658	-0.86960	0.14081	-0.91469	0.11648
0.8	-0.88468	0.14495	-0.93717	0.11091	-0.97706	0.07938	-1.00529	0.05025	-1.02275	0.02339
0.9	-0.99412	0.05184	-1.01511	0.01543	-1.02119	-0.01734	-1.01385	-0.04671	-0.99450	-0.07287
1.0	-0.99891	-0.04762	-0.98000	-0.08178	-0.94536	-0.11207	-0.89713	-0.13795	-0.83728	-0.15975
1.1	-0.89933	-0.14043	-0.83729	-0.17005	-0.76069	-0.19394	-0.67232	-0.21263	-0.57469	-0.22662
1.2	-0.70654	-0.21960	-0.60402	-0.23996	-0.49032	-0.25402	-0.36871	-0.26248	-0.24211	-0.26600
1.3	-0.44112	-0.27622	-0.30647	-0.28439	-0.16595	-0.28622	-0.02308	-0.28259	0.11904	-0.27430
1.4	-0.13076	-0.30473	0.02317	-0.29913	0.17634	-0.28779	0.32528	-0.27170	0.46713	-0.25179
1.5	0.19284	-0.30251	0.35055	-0.28321	0.50043	-0.25937	0.63945	-0.23204	0.76526	-0.20217
1.6	0.49723	-0.27007	0.64279	-0.23879	0.77400	-0.20466	0.88862	-0.16869	0.98505	-0.13176
1.7	0.75251	-0.21093	0.87174	-0.17081	0.97159	-0.12980	1.05081	-0.08875	1.10872	-0.04840
1.8	0.93415	-0.13112	1.01650	-0.08630	1.07660	-0.04253	1.11420	-0.00041	1.12951	0.03952
1.9	1.02526	-0.03858	1.06504	0.00640	1.08209	0.04872	1.07706	0.08802	1.05089	0.12403
2.0	1.01796	0.05766	1.01472	0.09855	0.99056	0.13565	0.94675	0.16890	0.88467	0.19828
2.1	0.91386	0.14844	0.87205	0.18175	0.81290	0.21083	0.73796	0.23585	0.64868	0.25698
2.2	0.72365	0.22526	0.65150	0.24876	0.56665	0.26827	0.47051	0.28412	0.36429	0.29662
2.3	0.46583	0.28104	0.37382	0.29400	0.27394	0.30379	0.16715	0.31083	0.05414	0.31545
2.4	0.16483	0.31080	0.06389	0.31394	-0.04065	0.31516	-0.14854	0.31487	-0.25979	0.31337
2.5	-0.15135	0.31199	-0.25150	0.30730	-0.35222	0.30214	-0.45396	0.29684	-0.55758	0.29159
2.6	-0.45375	0.28470	-0.54600	0.27503	-0.63727	0.26631	-0.72882	0.25872	-0.82224	0.25239
2.7	-0.71499	0.23157	-0.79559	0.22011	-0.87533	0.21079	-0.95610	0.20368	-1.04019	0.19880
2.8	-0.91171	0.15753	-0.98048	0.14728	-1.05001	0.13998	-1.12270	0.13567	-1.20144	0.13435
2.9	-1.02647	0.06932	-1.08634	0.06256	-1.14981	0.05918	-1.21970	0.05924	-1.29942	0.06279
3.0	-1.04923	-0.02509	-1.10523	-0.02717	-1.16859	-0.02579	-1.24245	-0.02081	-1.33078	-0.01199
3.1	-0.97816	-0.11728	-1.03605	-0.11475	-1.10566	-0.10898	-1.19049	-0.09962	-1.29517	-0.08618
π	-0.92222	-0.15298	-0.98247	-0.14886	-1.05637	-0.14166	-1.14759	-0.13091	-1.26110	-0.11599

	ε=10.0	k=0.6	ε=10.0	k=0.7	ε=10.0	k=0.8	ε=10.0	k=0.9	ε=10.0	k=1.0
0.0	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000	1.00000	0.00000
0.1	0.92108	0.09735	0.91622	0.09719	0.91137	0.09702	0.90653	0.09686	0.90169	0.09670
0.2	0.69704	0.17938	0.67922	0.17814	0.66153	0.17690	0.64396	0.17567	0.62651	0.17445
0.3	0.36375	0.23332	0.32928	0.22950	0.29537	0.22572	0.26203	0.22197	0.22924	0.21826
0.4	-0.02611	0.25105	-0.07496	0.24311	-0.12226	0.23531	-0.16804	0.22764	-0.21231	0.22011
0.5	-0.41213	0.23031	-0.46724	0.21730	-0.51920	0.20464	-0.56810	0.19232	-0.61402	0.18035
0.6	-0.73622	0.17485	-0.78532	0.15686	-0.82927	0.13960	-0.86828	0.12306	-0.90256	0.10722
0.7	-0.95177	0.09354	-0.98132	0.07195	-1.00378	0.05165	-1.01957	0.03260	-1.02910	0.01474
0.8	-1.03029	-0.00129	-1.02871	-0.02392	-1.01878	-0.04458	-1.00124	-0.06337	-0.97677	-0.08099
0.9	-0.96446	-0.09603	-0.92496	-0.11636	-0.87716	-0.13406	-0.82215	-0.14928	-0.76094	-0.16219
1.0	-0.76764	-0.17780	-0.68991	-0.19239	-0.60564	-0.20380	-0.51625	-0.21230	-0.42304	-0.21815
1.1	-0.47011	-0.23635	-0.36062	-0.24227	-0.24808	-0.24477	-0.13415	-0.24423	-0.02030	-0.24099
1.2	-0.11311	-0.26519	0.01603	-0.26062	0.14335	-0.25279	0.26714	-0.24219	0.38594	-0.22925
1.3	0.25785	-0.26210	0.39110	-0.24664	0.51697	-0.22855	0.63394	-0.20837	0.74082	-0.18659
1.4	0.59953	-0.22886	0.72060	-0.20365	0.82892	-0.17680	0.92339	-0.14890	1.00330	-0.12044
1.5	0.87606	-0.17059	0.97056	-0.13801	1.04795	-0.10507	1.10776	-0.07232	1.14989	-0.04022
1.6	1.06229	-0.09462	1.11979	-0.05793	1.15745	-0.02224	1.17553	0.01199	1.17460	0.04440
1.7	1.14513	-0.00935	1.16032	0.02787	1.15489	0.06286	1.12978	0.09528	1.08614	0.12489
1.8	1.12315	0.07685	1.09606	0.11125	1.04944	0.14247	0.98472	0.17033	0.90350	0.19470
1.9	1.00478	0.15655	0.94013	0.18544	0.85849	0.21060	0.76156	0.23201	0.65112	0.24966
2.0	0.80579	0.22379	0.71169	0.24544	0.60399	0.26329	0.48435	0.27741	0.35447	0.28789
2.1	0.54650	0.27438	0.43283	0.28820	0.30907	0.29855	0.17658	0.30559	0.03674	0.30942
2.2	0.24907	0.30599	0.12583	0.31244	-0.00454	0.31613	-0.14118	0.31718	-0.28324	0.31570
2.3	-0.06462	0.31792	-0.18883	0.31841	-0.31826	0.31705	-0.45272	0.31391	-0.59204	0.30902
2.4	-0.37470	0.31085	-0.49373	0.30745	-0.61746	0.30320	-0.74650	0.29808	-0.88147	0.29203
2.5	-0.66417	0.28651	-0.77505	0.28164	-0.89166	0.27692	-1.01549	0.27222	-1.14806	0.26734
2.6	-0.91944	0.24733	-1.02260	0.24348	-1.13409	0.24069	-1.25639	0.23873	-1.39203	0.23729
2.7	-1.13029	0.19614	-1.22945	0.19556	-1.34104	0.19687	-1.46869	0.19976	-1.61621	0.20381
2.8	-1.28966	0.13598	-1.39133	0.14045	-1.51099	0.14757	-1.65369	0.15702	-1.82493	0.16834
2.9	-1.39315	0.06990	-1.50595	0.08054	-1.64379	0.09462	-1.81356	0.11187	-2.02309	0.13186
3.0	-1.43864	0.00089	-1.57240	0.01804	-1.73989	-0.03954	-1.95050	0.06529	-2.21528	0.09497
3.1	-1.42586	-0.06815	-1.59062	-0.04502	-1.79976	-0.01637	-2.06608	0.01798	-2.40511	0.05798
π	-1.40363	-0.09623	-1.58409	-0.07096	-1.81403	-0.03960	-2.10801	-0.00178	-2.48392	0.04258

Insulation Breakdown as a Function of Area

L. R. HILL
MEMBER AIEE

P. L. SCHMIDT
ASSOCIATE AIEE

THE COMMERCIAL USE of insulation involves many complicated problems, one of which concerns the manner in which the distribution of breakdown strength changes with increasing area of insulation under test. Limited test data[1] indicates that the probability of failure at a low voltage increases as the area under test is increased. This theoretical problem was discussed in general terms by F. W. Peek[2] and in some detail by M. C. Holmes in two previous publications,[3,4] but the actual distribution of breakdown voltage was not obtained.

It is the purpose of the following discussion to obtain theoretically the distribution of breakdown voltages of large areas of insulation from the voltage breakdown distribution of small areas based on the variability of breakdown voltage from one small area to another. Electrode edge effects and poor contact effects, discussed elsewhere,[5] are neglected since this paper does not attempt to encompass a complete theory of dielectric breakdown nor to correlate the theory developed with published breakdown data[6] which, in general, are not statistically critical and include effects other than nonuniformity.

Insulation uniformity is most easily studied by statistical methods[7,8] which are briefly reviewed in the following paragraphs for those who do not make frequent use of this tool.

A series of insulation tests on samples taken from the same basic material will be distributed about an average value in an approximately normal manner; that is, the average value will occur most frequently and values on each side of the average will become decreasingly numerous, according to a definite law called the normal law. The probability of breakdown at any given voltage is the ratio of the number of samples failing at that voltage to the total number of tests. The standard deviation of a normal distribution is the numerical value which, when added to and subtracted from the average value, forms a set of limits which will include 68 per cent of the values in the distribution. When the insulation under test has a high variability, the ratio of the standard deviation to the average value is large, while insulation having good uniformity will give a distribution which has a small ratio of standard deviation to the average value. Typical values of the ratio of standard deviation to average value expressed in per cent for a few common insulating materials are given:

	Per Cent
Air	1– 2
Oil	4– 8
Paper	6–15
Laminated Mica	10–40

Distributions are said to be continuous if all values are possible and discontinuous if only discrete values are obtained. Usually continuous distributions are represented by curves and discontinuous distributions by histograms such that the area under the curve between any two values is proportional to the relative frequency of occurrence of values within that range. The familiar bell-shaped normal distribution curve is obtained in this manner. A more convenient way of studying normal distributions is to plot cumulative per cent of values at and below a given value against that value on a special type of graph paper called probability paper. This paper is so designed that a set of observations which are normally distributed form a straight line when plotted cumulatively since the scale on one of the co-ordinates is spaced in accordance with the normal law. The average value of a distribution which is represented by a straight line on this paper is that value where the line intersects the 50 cumulative per cent line, and the standard deviation is the difference between the average value and that value where the curve intersects the 16 or 84 cumulative per cent line.

It should be remembered that this useful normal law is a special case of the more fundamental binomial law which enables one to calculate the probability of occurrence of an event when certain specialized conditions are fulfilled. The binomial expression is perfectly general and makes no assumption concerning the form of the basic distribution involved.

We will show that theoretical cumulative probability of breakdown for n elemental areas in parallel is related to the cumulative probability of breakdown for an elemental area by the expression:

$$\text{Cum } P_{BD_n} = 1-(1-\text{cum } p)^n \qquad (1)$$

where

cum P_{BD_n}

is the cumulative probability of breakdown at R volts for n elemental areas in parallel and cum p is cumulative probability of breakdown for one elemental area at R volts.

Also, in those cases where discontinuous distributions are involved, the theoretical probability of breakdown at any given voltage is given by:

$$P_{BD_n} = \left[1-\left(1-\sum p_e[1,2,3,\ldots.i]\right)^n - \left(\sum p_e[1,2,3,\ldots.(i-1)]\right)\right] \qquad (2)$$

where

P_{BD_n}

is the probability of breakdown at R volts for n elemental areas in parallel, P_e is

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L. R. Hill is assistant manager, materials engineering department, and P. L. Schmidt is with the magnetic group of the same department, Westinghouse Electric Corporation, East Pittsburgh, Pa.

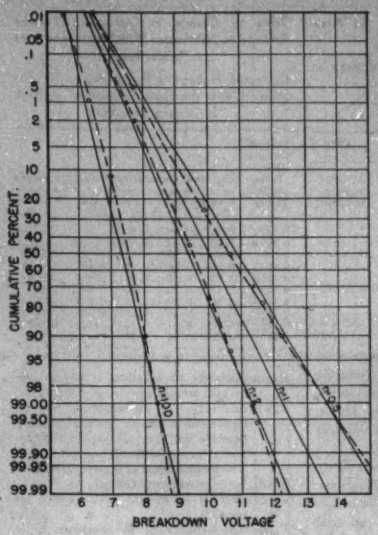

Figure 1 (left). The divergence from normal of distribution curves representing the voltage breakdown of 1/2, 2, and 100 elemental areas when the breakdown distribution of the original area ($n=1$) is normal

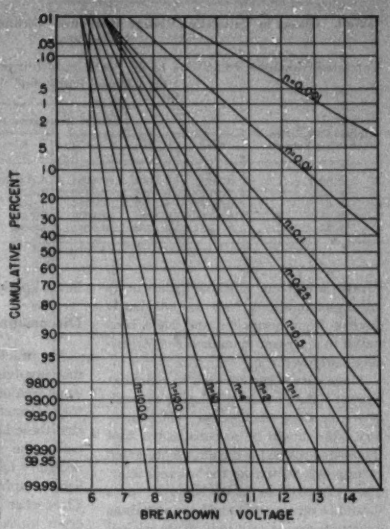

Figure 2. Voltage breakdown distributions for partial and multiple areas of an elemental area ($n=1$) when the distribution for the elemental area has an average value of ten units and a standard deviation of one unit

the probability of breakdown at voltages up to and including R volts for an elemental area and p_c is the probability of breakdown at voltages up to but not including R volts for n areas in parallel.

Tables giving theoretical expected breakdown voltage, normal distribution curves and typical examples for parallel area breakdown are included.

Theory

How can one determine the theoretical manner in which a breakdown distribution should change when n elemental areas are tested in parallel?

Given a distribution of breakdown values, all of which have associated with them a finite frequency of occurrence, the reasoning by which we arrive at the solution is as follows:

A. It is possible to obtain the probability of exactly *no* breakdown at any voltage by using the binomial expression

$$P_n(O) = C_o^n p^o (1-p)^{n-o} = (1-p)^n$$

where p is original probability of breakdown at the desired voltage for an elemental area. See Symbolism and Definitions of Terms.

B. This value, when subtracted from one might be expected to give the probability of at least one breakdown out of n samples or areas at the chosen voltage. However, this does not specifically exclude lower voltages on the remaining areas, which case would constitute a breakdown at a value below the desired voltage.

C. In order to exclude specifically lower voltages than that desired, it is necessary to use the cumulative frequency from the lowest value to and including the desired value as that value of p in the expression under A.

D. The expression

$$P_n(O) = C_o^n \left[\sum p[1,2,3,\ldots.i]\right]^o \times \left[1-\sum p[1,2,3,\ldots.i]\right]^{n-o}$$

$$= \left[1-\sum p[1,2,3,\ldots.i]\right]^n$$ then gives the probability of obtaining exactly *no* breakdown at or below the desired voltage.

E. Now if the value $P_n(O)$ is subtracted from one, we obtain the probability of at least one breakdown at or below the desired value, or in other words

$$\text{Cum } P_{BD_n} = 1-(1-\text{cum } p)^n \qquad (1)$$

F. However, if discontinuous distributions are involved, it is necessary to subtract not only $P_n(O)$ from one, but to also subtract the P_{BD_n} values for the $1,2,3,\ldots.(i-1)$th terms in the distribution. These P_{BD_n} values will be called the corrected probability values and will be represented by $P_c(1)$, $p_c(2)$, and so forth.

G. The probability of breakdown at any voltage when n elemental areas are under test then becomes:

$$P_{BD_n}\left[1-\left(1-\sum p_c[1,2,3,\ldots.i]\right)^n - \left(\sum p_c[1,2,3,\ldots.(i-1)]\right)\right] \qquad (2)$$

Application

In order that equation 1 may be easily used as an engineering tool, values of cumulative probability for several values of n times the original area under test are given in Tables I and II.

These theoretical probabilities of voltage breakdown for n elemental areas are not restricted by the shape of the original breakdown distributions but are applicable to any distribution.

It should be observed that the testing of several areas in parallel tends to shift the peak or most probable value of the distribution toward the lowest breakdown value. Distributions which are originally approximately normal, remain approximately normal but actually tend to skew somewhat. The extent of this distortion is less than the usual experimental error for determining the original breakdown distribution, but is of considerable theoretical interest. Since any area subject to dielectric breakdown can be considered to be made up of smaller elemental areas, the weakest of which determines the breakdown of the sample, it is obvious that actual breakdown distributions cannot be normal. Considering that the original distribution for an elemental area is normal, the divergence from normality for 1/2, 2 and 100 elemental areas is shown in Figure 1.

A useful plot, using a normal cumulative curve as the original distribution which has a standard deviation of one and a median value of ten, is shown in Figure 2. Cumulative probability curves for breakdown where 0.01, 0.1, 0.25, 0.5, 1, 2, 4, 10, 100 and 1,000 elemental areas are in

Table I. Change in Cumulative Voltage Breakdown Distribution With Increase in Area Under Test

Corrected Probability Points Are to Be Associated With the Same Breakdown Voltage as the Original Probability

Original Cumulative Probability Values	Corrected Cumulative Probability Values						
	$n=2$	3	4	5	10	100	1,000
0.0001	0.0002	0.0003	0.0004	0.00045	0.0009	0.0092	0.0879
0.005	0.0100	0.0150	0.0199	0.0248	0.0490	0.3950	0.9934
0.01	0.0199	0.0297	0.0394	0.0490	0.0955	0.6335	0.999967
0.25	0.4375	0.5741	0.6836	0.7627	0.9436		
0.50	0.7500	0.8750	0.9375	0.9687			

Table II. Change in Cumulative Voltage Breakdown Distribution With Decrease in Area Under Test

Corrected Probability Points Are to Be Associated With the Same Breakdown Voltage as Original Probability

Original Cumulative Probability Values	Corrected Cumulative Probability Values				
	$n=0.5$	0.25	0.1	0.01	0.001
0.0199	0.01	0.005	0.002		
0.0394	0.02	0.01	0.004	0.0004	
0.0879	0.045	0.023	0.009	0.0009	0.0001
0.0955	0.049	0.025	0.01	0.001	0.00012
0.3950	0.22	0.12	0.05	0.005	0.0005
0.6335	0.42	0.22	0.1	0.01	0.001
0.7500	0.50	0.29	0.13	0.014	0.0014
0.9375	0.75	0.50	0.24	0.027	0.0028
0.9436	0.76	0.51	0.25	0.028	0.0029
0.9934	0.92	0.72	0.39	0.05	0.005

parallel are given. Although the curves are approximations, they do not greatly deviate from normality and have been found to represent manufacturing experience fairly well.

These curves can be used to represent any set of data by simply placing an auxiliary scale along the abscissa. This auxiliary scale should be so constructed that the median value for the data represented corresponds to ten on the original scale. The standard deviation for the data should be represented on the new scale as a distance equal to one unit on the original scale.

For example, consider a voltage breakdown distribution having a median value of 4,500 volts and a standard deviation of 650 volts. The median value, 4,500 volts, will be placed at ten on the original scale, nine on the original scale now becomes 3,850 (4,500−650) and eleven on the original scale becomes 5,150 (4,500+650) and so forth. In this particular case, the median value for 100 elemental areas in parallel is found to be 2,875 volts and the standard deviation is 250 volts, using the curve for $n=100$, Figure 2.

It should be emphasized that the ratio of standard deviation to median value becomes smaller, although both decrease, with increasing number of parallel units. It is obvious that a small initial value of standard deviation is, in many cases, more desirable than a high median value. The value of standard deviation determines the rate at which the median decreases! It can be seen from this that high variability may be the source of severe manufacturing and field difficulties in the application of insulating materials.

Discussion

The practical application of insulation may involve areas much greater than the usual area of samples tested in the laboratory. Practical experience has shown that these large areas will fail at voltages below the sample test voltage, and a necessary factor of safety has been established empirically for many applications. Thus, when a certain lower limit of breakdown voltage is required for practical applications, the insulation must be such that the average and dispersion of laboratory tests indicates a much higher breakdown voltage.

The theoretical solution to this problem shows why the use of an empirical safety factor is necessary since the breakdown distribution for multiple areas shifts towards the lowest value in the original sample distribution.

Improvements in variability in basic insulation quality have allowed reduction of the safety factor on many occasions. Such basic quality changes can now be evaluated by testing small samples in the laboratory and predictions concerning insulation performance can be made through the application of the foregoing theory when the relative areas are known.

Symbolism and Definition of Terms

Cum P_{BD_n} = probability of breakdown for n elemental areas at and below a given voltage

cum p = probability of breakdown for an elemental area at and below a given voltage

P_e = probability of breakdown for an elemental area at any discrete voltage

P_c = probability of breakdown for n elemental areas in parallel at any discrete voltage; also called the corrected probability of breakdown for n elemental areas at a given voltage

R = discrete breakdown voltage

n = number of elemental areas tested in parallel

$P_n(m)$ = probability of m successes in n trials

$P_n(O)$ = probability of *no* successes in n trials

C_m^n = number of combinations of n things taken m at a time

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6. Apparent Dielectric Strength of Varnish Cambric, **Kennelly, Wiseman.** *Electrical World* (New York, N. Y.), volume 70, December 15, 1947, pages, 1138–41.

7. Mathematics of Statistics, **J. F. Kenney.** D. Van Nostrand Company, New York, N. Y., second edition, 1947, pages 109–33.

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Discussion

Hamilton Brooks (Westinghouse Electric Corporation, East Pittsburgh, Pa.): Hill and Schmidt have made a useful contribution by reducing a complicated mathematical relation to a simple set of easily read curves. Modern statistical mathematics can be a powerful engineering tool, but many statistical processes are laborious or appear formidable from a mathematical point of view and many engineers are often discouraged from applying them to every day problems. There is a real need, therefore, for development in engineering applications of statistics. We have found statistical theory valuable in analyzing and predicting the performance of capacitors made with impregnated paper insulation and are fortunate in having a large amount of experimental data to substantiate such relations as derived by the present authors.

A relation of great practical significance is that dielectric strength varies as the logarithm of the area. In effect, this means that one may test samples of two different

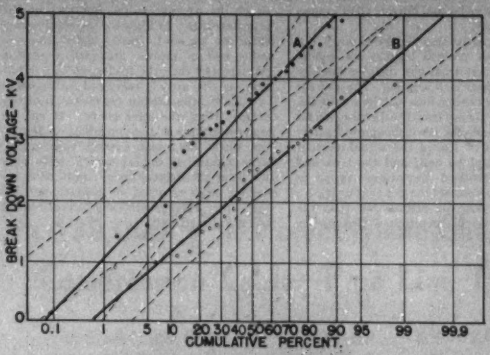

Figure 1. Distribution of breakdown voltage for number 30 enameled wire

Curve A—Samples six inches long
Curve B—Samples twenty-four inches long

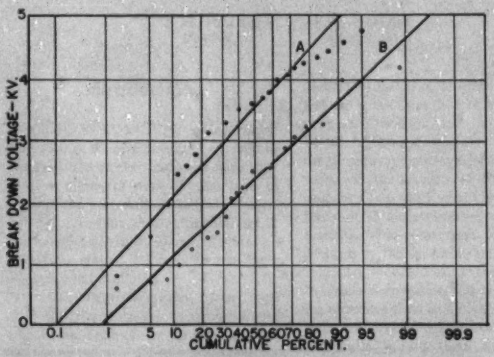

Figure 2. Additional data on same wire as shown in Figure 1

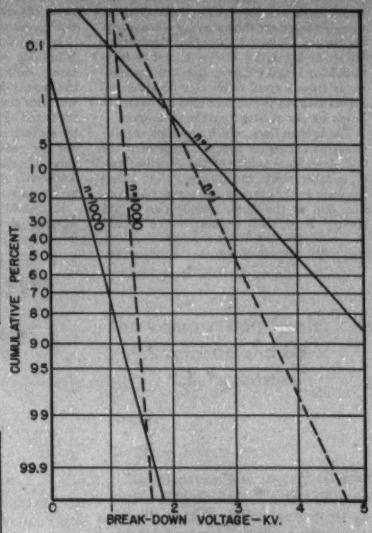

Figure 3. Curves showing the effect of variability on the breakdown distributions as the area is increased

areas and plot the voltage at which breakdown most frequently occurs against area on semilog paper. A straight line between the two points can then be projected to show the most probable strength of areas 100 or 1,000 times as large. This, of course, is not valid if we materially alter conditions in going to increased areas.

In assuming a simple normal distribution as a basis for their curves, the authors introduce certain errors, which they recognize but point out may be tolerated in attaining the desired simplicity. It should not be overlooked, however, that an important use of statistical analysis is in isolating and evaluating causes of variability. In this connection, it has been found worth considerable effort to rigorously derive the distribution of certain types of flaws in paper and calculate the resulting sampling distribution of defects in finished capacitors. It has been possible to obtain valuable improvements in insulating paper once the importance of particular kinds of imperfections was understood.

Benjamin Epstein (Carnegie Institute of Technology, Pittsburgh, Pa.): It may be of interest to point out that the problems considered in the paper are similar to those which occur in many different branches of science. The important thing from the physical point of view is whether or not we are dealing essentially with a "weakest link" situation. If such is the case then we have the key needed to find the relationship between the strength of specimens and their size. The term strength in this connection may be quite general in scope. We may have in mind mechanical strength, electrical strength, resistance of painted specimens to the corrosive effects of the atmosphere, or perhaps the life span of a device which ceases to function when any one of a number of vital parts breaks down. From a statistical point of view such phenomena, though different at first glance, are really equivalent since they lead in each instance to the same problem, namely the distribution of largest or smallest values in large samples. It turns out that the investigations by mathematical statisticians concerning the distribution of extreme values renders it possible for the engineer to make quantitative and not merely qualitative statements about size effects. The ability to make numerical estimates may be of considerable importance to design engineers. The role of the theory of extreme values in fracture phenomena is considered in a recent paper by the discussant.[1]

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1. STATISTICAL ASPECTS OF FRACTURE PROBLEMS, **Benjamin Epstein.** *Journal of Applied Physics* (New York, N. Y.) *19*, 1948, pages 140–7.

L. R. Hill, P. L. Schmidt: The authors feel that the discussions submitted by Epstein and Brooks bring out two important points which should be re-emphasized. First, that the scope of the statistical theory presented is not confined to the study of insulation breakdown as a function of area, but is applicable to all problems which involve the failure of a material due to the failure of its "weakest link." In all cases the average strength and variability decrease as the area increases. Second, that the theoretical treatment presented is in agreement with a large amount of engineering data on capacitor paper performance.

The authors have also found agreement between the theoretical and actual change in the distribution of breakdown voltages for enameled copper wire as the area under test is increased. Samples of wire six and twenty-four inches long, respectively, were submerged in a mercury bath and an increasing voltage was applied between the mercury and the copper. The voltage at which breakdown occurred for thirty samples of

each length was observed and the distributions obtained are shown on the accompanying curves. Figure 1 gives the distributions for six-inch lengths, Curve A, and twenty-four inch lengths Curve B taken consecutively from a spool of number 30 wire, and Figure 2 shows the data obtained from a different section of wire from the same spool.

The broken lines, which envelope the distribution curves on Figure 3, represent the 0.95 confidence limits of the distributions, or, in other words, the chance is 19 to 1 that the error in the average and standard deviation due to the finite sample size is not greater than that amount which would cause the curves to fall outside their respective envelopes.

It can be seen that the breakdown distributions for six-inch lengths are significantly different from the distributions for the twenty-four inch lengths. The average value for the twenty-four inch long samples, predicted by theory from the distribution for the six inch samples, is within approximately five per cent or less of the actual average value obtained by test, and the true and predicted standard deviations agree to within approximately ten per cent or less.

It should also be emphasized that the standard deviation determines the rate at which the average value decreases as the area increases. In Figure 3 the solid curve, indicated as $n=1$, has a higher average value and a greater standard deviation (variability) than the broken curve. However, as the area is increased 1,000 times ($n=1{,}000$), the broken curve has a higher average than the solid curve. It can be easily seen from this example that an insulation which has a high average value and a large variability on laboratory tests may not be as satisfactory in an application involving large areas as an insulation which has a lower average but a smaller variability on laboratory tests.

A 138,000-Volt Polyethylene-Sheathed Compression Cable—Pipe Line Type

J. E. McCORMACK
FELLOW AIEE

C. T. HATCHER
MEMBER AIEE

K. S. WYATT
FELLOW AIEE

W. A. DelMAR
FELLOW AIEE

E. J. MERRELL
ASSOCIATE AIEE

J. H. PALMER
MEMBER AIEE

E. F. DeTURK
MEMBER AIEE

Introduction*

COMPRESSION-TYPE cable for high-voltage transmission has been used in England and on the Continent since 1932 and experimental work on this type of cable in the United States was reported on in 1939. In 1932 an experimental installation was made in the United States of an Oilostatic system, followed in 1934 by a commercial installation.

In the compression-cable system used in England and on the Continent a lead sheath on each of the single-conductor cables served as a flexible membrane for transmitting pressure to the cable insulation. The use of a lead sheath entailed two disadvantages for high voltage single conductor cables, namely, increased losses due to sheath currents and possible rupture of the lead sheath under repeated load cycles. In the usual high-pressure oil-type pipe systems and some high-pressure gas systems the lead sheath is removed prior to installation in the pipe, thereby subjecting the nonjacketed cable to contact with the atmosphere during installation and to contact with the inside of the pipe or its internal coating and the oil in the pipe.

The elimination of the lead sheath with its inherent disadvantages, on compression cable and the possibility of installing some type of jacketed cable in the field appealed to the companies whose representatives are presenting this paper.

During the early part of 1946 it was decided to install a tie line between the systems of the Consolidated Edison Company of New York, Inc., and the Long Island Lighting Company. A study of the tie requirements revealed that 138,000 volts was the economical voltage to be used for a line which would be required to transmit 75 megawatts between the two systems. For some time prior to this decision by the operating companies, the engineers of the Phelps Dodge Copper Products Corporation had been conducting extensive tests on a compression-type cable and presented data to the operating companies to aid them in making a decision regarding the type of circuit to be installed.

In order to further the development of high-pressure pipe-type cables and to obtain the advantages mentioned before, agreement was reached to install a polyethylene-sheathed compression cable. In making this decision the economic aspect was given full consideration.

In the following sections the manufacturer and the users are presenting a complete description of all the phases of work involved in the design, manufacture and installation of this newest type, high voltage, pipe cable.

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J. E. McCormack is outside plant engineer and C. T. Hatcher is division engineer, both with the Consolidated Edison Company of New York, Inc., New York, N. Y. K. S. Wyatt is engineer of cables, W. A. DelMar, chief engineer, E. J. Merrell, head of electrical research laboratory, and J. H. Palmer, electrical engineer, all with the Habirshaw Division of the Phelps Dodge Products Corporation, Yonkers, N. Y. E. F. DeTurk is an electrical engineer with the Long Island Lighting Company, Mineola, N. Y.

The Compression Cable: Its Evolution and Basic Characteristics*

History

European Installations

The effectiveness of keeping oil-paper insulation at all times filled with oil under pressure in order to suppress ionization and failure was brilliantly demonstrated by Luigi Emanueli in his researches, commencing about 1923, and culminating in 1927 in extensive installations of the Pirelli oil-filled cable in New York and Chicago at 138,000 volts. This achievement was emphasized by the background of operating failures of paper cables at only 33 kv and less which most countries were then experiencing. But although twenty years of operating experience have established the principle of maintaining the impregnating oil under pressure, accessories of the oil-filled cable system are numerous, and may require much servicing.

In late 1926 Martin Hochstadter commenced research in London at the Enfield Cable Works, Ltd. to find a simpler and more economical way of making high-voltage cable. Departing from Emanueli's principle, he applied radial pressure to the insulation through a diaphragm. First, he tried pressure on the insulation from the conductor outward. Propagation of pressure across the dielectric from the inside out, however, is incomplete. He then tried applying pressure from the outside in—that is, an ordinary cable with lead sheath was subjected to external pressure. At 200 pounds per square inch ionization was effectively suppressed. But after only a few load cycles the lead sheath would stretch, fold over, and crack. The final solution came in making the cable out-of-round in cross-section, and in reinforcing

* J. E. McCormack.

* K. S. Wyatt.

the lead sheath with thin bronze tapes, so that its periphery was fixed in length. These features enabled the sheath to expand and contract in volume content, with varying load, without being stretched and folded. It was then found that cables with 66 kv insulation could be rated at 132 kv and tested at 228 kv on load cycles to 90 degrees centigrade with complete freedom from ionization and breakdown. Other accelerated tests in a nonoxidizing pressure medium showed the reinforced lead sheath should perform satisfactorily for 50 years or more. The new system was called The Pressure Cable and was described before the Royal Society of Arts by Hochstadter, Vogel and Bowden, in December 1931.[1]

The first commercial pressure cable, $2^1/_2$ miles long, was installed in London in 1932.[2] A 3-core cable, impregnated with rosin compound and insulated with 33 kv solid cable thickness, with a triangular lead sheath over-all (*HSO*), was pulled into a steel pipe and operated under 200 pounds nitrogen pressure at 66 kv. This cable has operated satisfactorily ever since without incident, even though during the German blitz of 1940–41 a bomb displaced and dented the pipe, and even though other nearby cables, some at 66 kv all failed. However, the original corrosion protection was inadequate and the pipe later had to be recoated.

An experimental installation for a type test of 66 kv pressure cable was made at Wood Lane, London, by Callender's Cable and Construction Company in early 1933. After three years' testing under load cycle at over-voltages, Dr. D. M. Robinson pronounced the system completely satisfactory, except for some corrosion of the steel pipe, which again had been inadequately protected.

Another experimental installation 110 yards in length and at 260 kv was made at the KEMA Laboratories in Arnheim, Holland, in 1936. The maximum stress employed was nearly 400 volts per mil. The cable was loaded to 90–95 degrees centigrade copper. During installation air got into the cable at a joint and caused oxidation and copper soap formation, and this finally resulted in a failure near the joint. A second cable was made to replace the first in which a larger copper was used so as to limit maximum temperature on the load cycle to a reasonable value, but the insulation thickness was still further reduced to give a maximum stress of 425 volts per mil. Aside from terminal trouble, the cable operated satisfactorily for 250 load cycles. Since solid cables operate at maximum stress of 70 volts per mil and oil-filled cable at 245 volts per mil, the foregoing test at 425 volts per mil was regarded as an achievement.

After the London cable installation had operated successfully for two years, an increasing number of pressure cables in steel pipe were installed in Europe. Copenhagen, Denmark, and Oslo, Norway, were among the first. By then the German authorities realized the ruggedness of the steel pipe construction under aerial warfare, and installations followed at Muldenstein, Stettin, Brunswick, Barmen, Elberfeld, Munich, Watenstett, and Magdeburg, until by 1940 there were 14 installations in all.

During the war, the pace was accelerated and despite the acute shortage of steel, other pipe installations went in at Gottingen, Zschornewitz, Reichswerke, Berlin, Dessau, Finkenheerd, Heidebruck, Hamburg, and Salzburg, bringing the total to 24, of which nine were at 100 kv or over. Noteworthy is the tendency to repeat, where a pipe cable has once been laid; for example, the installations at Brunswick and Elberfeld were each followed by two others, and that at Munich by a second.

In England, however, there was prejudice against the steel pipe because of corrosion troubles. When the pipe was replaced with a reinforced lead sheath applied over the cable at the factory, the self-contained Pressure Cable, as it was called, found immediate acceptance.[3] Installations were made in 1940 and 1941 at London, Ferrybridge, Rotherham and Doncaster, all at 66 kv. Since the war other installations have gone in, one at 132 kv at Osbaldwick, Yorkshire, two at Newcastle, and one at Sidney, making a total of eight self-contained compression cables. A short length is operating experimentally at 264 kv.

All in all, there are now 32 installations of the pressure cable abroad, all operating satisfactorily, with the exception of one or two in Europe damaged by bombs. This high-tension cable system is therefore second in operating mileage and experience to oil-filled cable. There has been one failure, and this occurred on the Copenhagen line next to a joint as a result of differential expansion of the steel armoring and the lead sheath, at a point where the armor was improperly anchored at a joint. This trouble necessitated replacement of a considerable length of cable. Some corrosion of the steel pipe was encountered at Oslo, but was corrected at an early date by cathodic protection. One of these lines runs in mountainous areas for thousands of feet down 25 per cent grades. Several installations have appreciable vertical risers at terminal ends, one to a height of 80 feet. With the exceptions noted, operation has been satisfactory and maintenance negligible.

These European installations all employed nitrogen gas as the pressure medium. The pressure was maintained at 200 pounds per square inch. The compressed gas acted upon the reinforced lead cable sheath but did not make direct contact with the oil-paper insulation. However, oil was sometimes used as the compression medium in the single-core tails which were housed in copper tubes leading to the terminals at either end. Such oil was separated from the insulation by the lead sheath as was the gas. In the 1932 London line, for example, both oil and gas were employed as the compression medium; since the tails were only 50 feet long, only 300 feet of cable were compressed with oil. Gas was preferred because it did not involve complicated expansion reservoirs and semi-stop joints, and because pressure is uniform from end to end of the line irrespective of profile and of load variation. In the experimental work, a sheath of oval form was used for single-core cable, and of triangular form for 3-conductor cables. About 1936 the single-core oval cable with each core separately sheathed (*HSL*) became the accepted practice. Because the cable was protected by a sheath, none of the steel pipe lines was cleaned or coated inside.

It was not long after the initial success of the pressure cable before other cable systems in steel pipes or in reinforced lead sheath filled with oil or gas at 200 pounds per square inch began to appear. They had only one major point of difference, namely, the oil or gas pressure medium was not separated by a sheath, but was in direct contact with the insulation. Many of these systems were called pressure cables. In order to distinguish the original system from these newcomers, the name, the pressure cable, was changed to the compression cable. The term "compression" brings out very aptly the essential point of difference between the systems. In the compression cable the pressure medium acts on the cable sheath, and the latter in turn acts on the insulation, compressing it layer against layer with a positive piston action; whereas in the cable systems without sheath the pressure medium merely runs in and out of the insulation by means of the interlayer path as the pressure fluctuates. Of course, the sheath is quite useful also for protecting the insulation against moisture or other contamination, and in the case of

gas-filled pipe it prevents the oil ratchetting out of the insulation on slopes or vertical risers.

Simplification of Design

Although successful abroad, steel pipe cables at extra high voltage found no general acceptance in this country until recently, partly because of limited plant expansion in the 1930's, partly because the economics of pipe systems had not been demonstrated, and because utility engineers were concerned over the methods of protecting the steel pipe against corrosion. The clumsy and expensive European compression cable design with its steel-armored cable and complicated joints further contributed to this lack of interest.

It should be noted that in 1934 the Pennsylvania Railroad installed a 132-kv Oilostatic cable in a steel pipe. The system proved entirely satisfactory except for some corrosion trouble, but it was not until 1940 that a second extra-high voltage installation was made at Burlington, New Jersey, at 138 kv.

The first step in simplification was taken in 1940 by L. I. Komives in an experimental field installation of compression cable at 120 kv at Detroit.[4] He dispensed with the steel armor, bedding and binder tape around the three *HSL* cores, and used a simple solid-type joint. The three cores were still triplexed, however. The pipe was filled with nitrogen at 200 pounds per square inch. The results of several years of loading tests showed complete stability even though the lead sheath of one core had been purposely punched with holes at 25-foot intervals. When a commercial seven mile line of 120-kv cable was installed at Detroit in 1941, the compression cable design was not selected, but instead a cable without sheath in a steel pipe filled with 200 pounds per square inch nitrogen. In this cable the three single cores were not triplexed, it being impossible to strip the lead from the single cores of a triplexed cable.

The reason for the non-selection of the compression cable lay not in its electrical characteristics, which are superior to those of most other types, but in its economics as then designed. The great weight of the lead sheath in the triplexed design made for only 800 feet pulling lengths as against 2,000 feet for cables not triplexed and with sheath stripped off. This more than doubled the number of joints and manholes. In addition, with large conductor sizes such as are usually used in this country, appreciable losses occur in the lead sheath which would sometimes necessitate a larger size conductor. The energy losses in the sheath also represent an appreciable annual charge. Above all, utility engineers, as a result of long experience, did not trust lead.

The European compression cable design was clearly uneconomic for our conditions, and at this time serious thought was given to the development of another type of steel pipe cable without sheath. But with the knowledge of the actual conditions of installation in the field, the effects of accidental rust, mud, asphalt and moisture on the unprotected insulation in the pipe, the difficulties of pulling an unsheathed cable during long periods of rainy weather or high humidity, and tedious treating of each section of cable during installation, it became more and more apparent that a manufacturing operation that ought to be done in the factory would have to be done in the field under conditions difficult to control. Not only were these conditions a hazard to the life of the cable, but they might greatly increase the over-all cost of an installation in spite of the cable itself being cheaper. In those cases where the pipe was filled with gas, the factor of safety (long-time breakdown voltage to operating voltage) dropped to half or less. Even traces of moisture in the nitrogen gas at the feed point would in time weaken the insulation at such a location. On slopes or vertical risers the oil would ratchet out and drain from the insulation. If the pressure were lost, the cable would immediately have to be taken out of service. And finally, an oil-filled terminal could no longer be used even if a stop gland were employed at the terminal base, as the oil would run through the insulation and out into the pipe, necessitating a gas-filled terminal of inferior electrical strength.

The European compression cable therefore was examined once again with a view to further simplification. In addition to the improvements due to Komives, it was decided to replace the heavy lead sheath with a plastic sheath. After a search of available materials, and after experimental work with the more promising, polyethylene was selected. Three lengths of 132-kv cable were sheathed with this light-weight, tough and elastic material, using a 63-mil wall. The plastic sheath was then reinforced with two 5-mil bronze tapes. After a series of tests, to be described in another section of this paper, the new reinforced polyethylene sheath was found to be a solution of the problem.

In order to increase pulling lengths and reduce the number of manholes and joints, two further steps were taken. Over each core three *D*-shaped copper skid wires were applied with a lay of 12 inches, as compared with the 2-wire 3-inch lay which had been used heretofore. Then triplexing was abandoned, and it was decided to pull into the pipe three single-core cables, each from a separate reel.

No difficulties have been experienced from bends as it has been possible to keep the radius in all cases, except at terminals, to at least 33 feet. European cities, with their narrower and more crooked streets, have required bends of 6 feet radius, and these have caused no trouble with the size cables used.

The replacement of the lead sheath with a polyethylene sheath in many cases about halved the weight of the cable, making it possible to pull 2,000 feet instead of 800 to 1,000. The long lay skid wires reduced the coefficient of friction from 0.39 to 0.23, still further increasing the possible pulling length from 2,000 feet to 3,000 feet.

The Plastic-Sheathed Compression Cable

The new construction as used on the New York–Long Island Line is as follows: Three impregnated paper-insulated copper conductors with bronze reinforced plastic sheath are pulled into a steel pipeline laid under the streets. The pipe is filled with oil at 200 pounds per square inch.

An oil of 3,000–4,000 Saybolt universal seconds at 100 degrees Fahrenheit is used to impregnate the insulation. This viscosity gives maximum electrical and chemical stability consistent with mass-impregnation methods. Where oil is used in the pipe as the compression medium, the mobility requirements of the system dictate the use of a low-viscosity oil, around 700 Saybolt universal seconds at 100 degrees Fahrenheit. Thus, owing to the presence of the sheath it is possible to use a high-viscosity oil in the insulation such as will give stable electrical characteristics, and a comparatively low-viscosity oil as the pressure medium such as will give good low-temperature flow characteristics.

The pipe is protected against corrosion by 1/2-inch thick extruded asphaltic coating. The adoption of an extruded pipe coating has met the objections to steel pipe which interfered with the early adoption of this system. The installation of 7 miles of pipe cable at Detroit has demonstrated the effectiveness of this type of protection by five years of service.

Joints are essentially a carefully made solid joint, the hand applied insulation consisting of oil-impregnated paper tape, covered with polyethylene tape. They

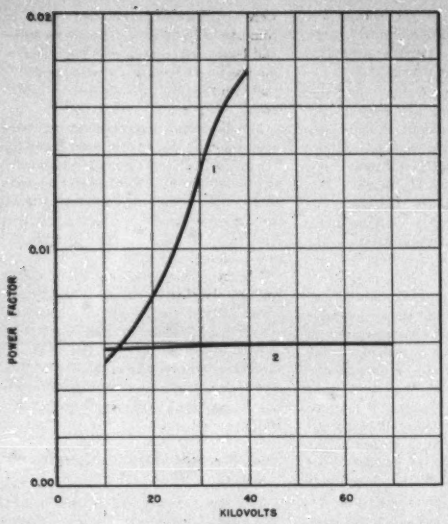


Figure 1 (left). Effect of applying pressure to a 3-core 66-kv solid cable exhibiting serious ionization as a result of overheating

1—before, 2—after application of 12 atmospheres pressure external to the lead sheath for five hours

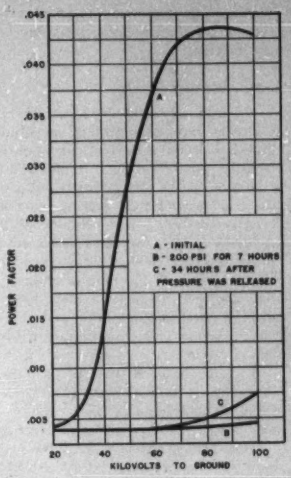


Figure 2. Single core 66-kv HSL 380,000 circular mils subsequent to overheating and draining

are described in detail in another section of this paper. Semistop joints, excepting at terminal ends, are not required on the compression cable system, since treating and filling of the entire line is carried out in one operation.

The compression cable itself, apart from the compression medium, requires no expansion reservoirs, as it is automatically self-compensating. The oil pressure medium requires an expansion reservoir with pumps installed at one end or both ends of the line.

Terminals are of the standard oil-filled type with an inner tube carrying the high pressure. A small compensator is mounted on each terminal which by means of a flexible membrane separates the oil pressure medium in the pipe from the oil in the cable, and at the same time communicates the pressure in the pipe to the oil in the terminal. A gland or stop at the base of the porcelain prevents the oil in the terminal from draining into the pipe. There is no appreciable pressure drop across this gland because the compensator maintains a balanced pressure on each side.

Basic Characteristics

Effect of Pressure

The application of 180 pounds per square inch pressure to a solid lead-sheathed cable exhibiting serious ionization is shown in Figure 1. After a few hours ionization has completely disappeared. Even when a cable is practically "dry" or free from compound, pressure external to the lead sheath will suppress ionization, albeit more slowly than with a good cable, Figure 2. Curve *A* represents ionization after draining, and curve *B* shows the absence of ionization after the application of 200 pounds per square inch for seven hours. Curve *C* shows that the effect of pressure is persistent long after it has been removed; even then at operating voltage there is no discernible ionization.

Ionization is so completely eliminated by high pressure external to the sheath that the maximum dielectric strength inherent in oil-impregnated paper is realized. Using the time-voltage test as a criterion, a solid cable reaches a breakdown value of about 400 volts per mil maximum stress for indefinitely long application of voltage, Curve *A*, Figure 3. If 200 pounds per square inch are applied external to the sheath, an asymptotic value of 1,000 volts per mil or higher is attained even without strand shielding, curve *B*. Thus the factor of safety (long-time breakdown strength/operating voltage) in a compression cable is four times or more. Owing to the sheath keeping the insulation filled with oil, and protecting the insulation from contamination, this factor of safety represents, not merely an initial value, but a value permanent throughout life.

The compression effect is operative under severe and prolonged load cycles. For example, a 132-kv cable was given 105 load cycles under 200 pounds per square inch to conductor temperatures of 80 to 93 degrees centigrade at a maximum stress of 375 volts per mil. The test occupied over a year, during which the cable was continuously under voltage. Ionization was completely absent both before and after test. The only change was improvement of the base power factor, probably due to minute pores in the paper becoming filled with oil, Figure 4.

On a lead-sheathed compression cable, as normally made, the critical pressure at which ionization disappears, when pressure is first applied, is 90 to 100 pounds per square inch. For a polyethylene-sheathed cable the critical pressure is lower, between 50 and 70 pounds per square inch. A pressure of 200 pounds per square inch has been standardized for normal operations so as to give a suitable margin of safety.

Since pressure completely suppresses ionization, breakdown occurs, if the voltage be raised sufficiently high, not by the usual tracking or dendritic mechanism, but by a straight-through puncture.

Propagation of Pressure

The compression cable is made oval in cross-section for two reasons, to accommodate oil expansion, and to make the insulation responsive to the applied external pressure. When the cable is loaded and heats, the insulation expands

slightly on its minor axis and contracts correspondingly on its major axis, that is to say, the oval cross-section becomes more nearly circular. When load is removed and the cable cools, the external pressure acting on the flat sides of the oval forces the insulation back to its original position. This follows from the fact that a tube of fixed periphery contains maximum volume when it is circular and minimum volume when it is flattened out. The proper action of the oval insulation in accommodating itself to changes in volume is conditional upon the use of a sheath which does not stretch, hence the use of bronze reinforcement. Other reasons for the bronze tapes are given in another section of this paper. The latter also insures that sheath and insulation breathe symmetrically. The reinforced sheath is designed to withstand 500 pounds per square inch internal pressure without rupture.

When 200 pounds per square inch are applied to a lead-sheathed oval cable, there may be as much as 30 or 40 pounds per square inch drop across the sheath during rapid changes in load. With the polyethylene sheath there is a negligible pressure drop, and hence 200 pounds per square inch external pressure will raise the oil pressure within the insulation to approximately 200 pounds per square inch. It should be emphasized that the reinforced polyethylene sheath is subjected at all times to a balanced pressure, that is, if the external pressure rises and falls, the internal oil pressure will rise or fall by an equal amount simultaneously. It will be recognized that if the oil in the insulation is at 200 pounds per square inch, any traces of gas film that might conceivably be present in the cable as manufactured will go into solution within a matter of minutes.

When the cable with polyethylene sheath cools, the external pressure will be communicated without appreciable drop and without time lag to the conductor. But upon sudden loading of the cable there may be local increases of pressure in the vicinity of the conductor. It is here that the oval form is again beneficial in that serious pressure differentials cannot be built up. In solid cable of circular cross-section, pressures at the conductor of 125 pounds per square inch have been observed when maximum load has been thrown on a cold cable[5] and indeed it is possible to burst the paper tapes at the conductor in this manner. Although solid type oil-paper insulation is thought to expand as a whole up to some limiting temperature, for example, about 60 degrees centigrade, due to the elastic properties of the paper and the sealing effect of the viscous oil, nevertheless an irreversible change may take place if the cable is heated to 70 degrees centigrade or higher, as is shown by increase in ionization factor. This is due to the oil at the center flowing outward through the interlayer spaces. Before such flow can take place, in a cable of circular cross-section, very considerable pressures may be built up at the center of the cable.

Now consider a cable of oval cross-section. When the oil at the center expands, the paper tapes distend along the minor axis with no appreciable increase in pressure. It is for this reason that an oval cable behaves reversibly on the load cycle at temperatures at least 10 degrees centigrade higher than a round cable.[6] Callender's Cable and Construction Company manufactured and installed 66-kv solid cable of oval form prior to 1936. The only reason that such cable did not gain general currency was that oil-filled cable was coming into general use at this voltage. At lower voltages sector-type cable is usual and this, of course, is essentially oval in form; in fact, the markedly superior load cycle stability of the oval form probably accounts for the dielectric strength of sector type cables being equal to that of cables with round conductors despite the sharp radius of the sector conductor shoulders.

Operation without Pressure

Should the external pressure be lost, the compression cable may be safely operated without pressure for periods up

Figure 3. Voltage versus time-to-breakdown for solid cable with and without pressure

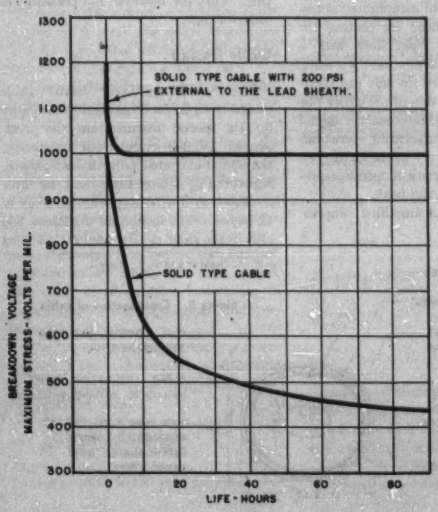
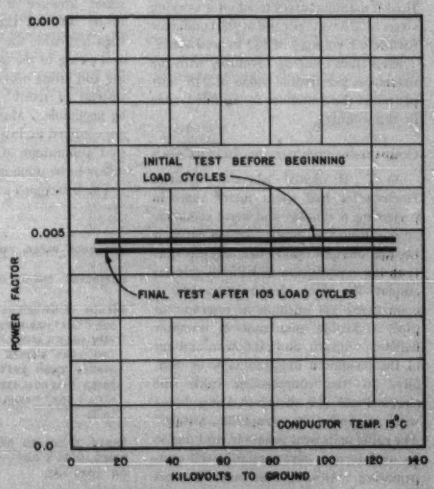


Figure 4. Effect of load cycling on ionization of 132-kv compression cable



to 48 hours. This valuable characteristic arises from the fact that the sheath and insulation have been compacted, and as a result the pressure effect persists even when pressure has been removed, curve *C*, Figure 2. Apart from this consideration, the normal life of the cable, operated as a solid cable without pressure, is over 1,000 hours, curve *A*, Figure 3.

An unexpected feature of the polyethylene-sheathed compression cable, described in another section of this paper, adds greatly to the safety of 48-hour operation without pressure. This is the complete mechanical reversibility of the polyethylene sheath cable construction. Even with no external pressure, the cable will return of its own accord to its normal ovality under no-load conditions from the more nearly circular shape it assumes under full load. Hence even without pressure, little or no ionization develops for considerable periods of time.

Working Stress

The early compression cables were designed with a limit of 210 volts per mil maximum stress. As cable materials and construction improved, this value was raised. The 260-kv Arnhem experimental cable operated at 425 volts per mil for 250-load cycles to 68 degrees centigrade satisfactorily. In 1939, in London, compression cable manufacturers agreed on a maximum stress of 300 volts per mil, and several installations in 1940 and following employed a value of 260 volts per mil. More recently the economics of the self-contained compression cable have forced British manufacturers to adopt a working stress of 375 volts per mil at the conductor surface for voltages of 132 kv and above. These values compare favorably with the maximum permissible value of 245 volts per mil at the conductor for oil-filled cable in this country.

Comparison with the Oil-Filled Cable

As T. R. Scott[7] has shown, after Hochstadter had spent many years in perfecting a simpler and more economic system than the Pirelli oil-filled cable, it became obvious upon close analysis that both the compression and Pirelli systems employ the same elements, namely, a compressed gas cushion in reservoir or pipe, a flexible membrane in reservoir bellows or sheath, and oil flow in and out of the insulation longitudinally or radially. In the compression cable the sheath itself is a distributed membrane with an almost imperceptible "stroke." The cable is its own reservoir, and due to its oval form is automatically self-compensating. Although the same elements were employed, simplicity and freedom from a multiplicity of accessories had been achieved in the new system.

Review

Arising from the fact that extra-high voltage underground cables are frequently tied to overhead lines and must be capable of withstanding severe transients, and arising from the present economic pressure to reduce underground costs, a successful extra high-voltage cable system must meet five main requirements. These are

1. A high factor of safety against breakdown.
2. High factor of safety must be permanent throughout operating life.
3. Low installed cost.
4. Relative absence of maintenance costs.
5. The cable must be easy to install, requiring a minimum of trained supervisory personnel.

It is believed that the polyethylene-sheathed compression cable fully meets all five requirements. The factor of safety against breakdown is exceptionally high. The sheath, applied at the factory immediately the cable is taken from the impregnating tanks, makes the cable a factory-sealed unit throughout shipment, installation and operating life, hence the high initial factor of safety is maintained permanently. The installed cost is a minimum owing to efficient use of cable materials, absence of sheath stripping operations, absence of complicated accessories, absence of need for coating the inside wall of the pipe, since with a sheathed cable the pipe is merely a duct; and owing to the need for only one treating and filling operation of the entire line instead of many. Maintenance should be negligible. Manufacturing operations are confined exclusively to the factory, so that a minimum of critical operations remains to be done in the field.

The redesigned and simplified compression cable, as constructed for the 138-kv New York–Long Island installation, appears to have met the needs of the industry for a system economical both in initial cost and maintenance.

Characteristics and Development of the New York–Long Island Poleythylene Sheathed Compression Cable*

Purpose of the Tests

The realization that the lead sheath of a compression cable might be replaced by one of polyethylene, grew out of the discovery that polyethylene, supported under pressure so as to prevent swelling, could be maintained indefinitely in oil at much higher temperatures than had hitherto been considered safe.

This led to the manufacture and testing of a series of polyethylene sheathed cables, to ascertain the characteristics required for a successful design.

The support adopted was a pair of suitably cushioned bronze tapes over the sheath.

The following summarized tests are a selection from the large number made on compression cable both before and after the manufacture of the New York–Long Island tie line was started. The earlier tests were primarily to guide design; the later ones, to check operating characteristics. They are, however, not presented in order of date.

Cable Design

The New York–Long Island cable design is shown in cross-section in Figure 5. Its special features are the oval-shaped 350,000 circular-mil conductor, the 63-mil extruded polyethylene sheath, supported by bronze tapes, and the three *D*-shaped skid wires applied with a lay of 12 inches. The insulation thickness, 560 mils, is the same as that used for oil-filled

* W. A. DelMar and E. J. Merrell.

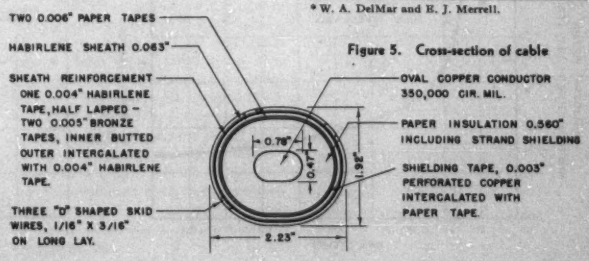


Figure 5. Cross-section of cable

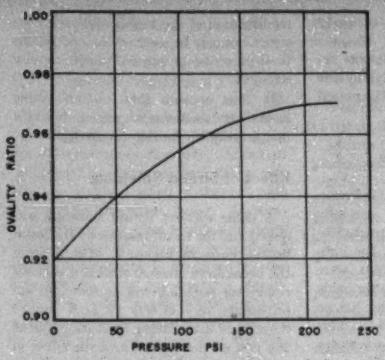


Figure 6 (left). Change of ovality ratio with internal pressure

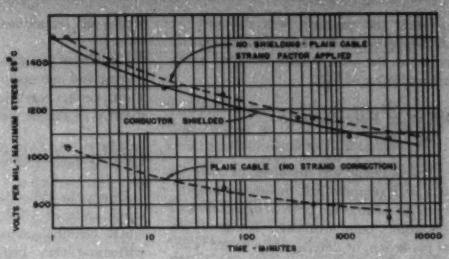


Figure 7. Voltage stress as a function of time

cables of the same voltage. It will be noted that the ovality ratio, that is, the ratio of the minor to the major axis, over the bronze tapes, is 0.85 and that is the value aimed at in extrusion. In the actual cables, however, the ovality ratio at room temperature and without external pressure becomes about 0.90 due to the elasticity of the sheath, a characteristic of considerable value in the event of loss of pressure.

The New York–Long Island line consists of three such cables installed in a steel tubing (5.5 inches inside diameter, 6.0 inches outside diameter) filled with oil at 200 pounds per square inch.

Manufacture

The manufacture of compression cable follows, in general, the usual procedures for solid-type paper insulated cables up to the point of application of the sheath.

The oval-shaped conductor is formed by roller dies in much the same way as sector conductors.

Upon removal of the cable from the impregnating tank it is taken immediately to the polyethylene extrusion machine which is equipped with dies especially designed for application of large diameter sheaths. The cable is under strict temperature control from behind the extrusion machine until it reaches the take-up reel, this being of the utmost importance to obtain uniform sheath thickness and freedom from strains.

The polyethylene and bronze tapes were applied in the next operation and the skid wires in a final one, bringing the cable to the shipping reels.

Tests were made on each length, without pressure, as follows:

129,000 volts for 15 minutes
Ionization at 20 to 100 volts per mil

Tests on lengths of 35 feet under shield were made with 200 pounds per square inch oil pressure for power factor versus temperature up to 80 degrees centigrade and the same lengths used for breakdown voltage in accordance with Association of Edison Illuminating Companies' (AEIC) procedure.

This last test was made as follows:

6 hours at 190 kv
3 hours at 224 kv
3, 6, or 12 hours at 275 kv

No failures occurred. One test included a splice. No higher voltage was attempted due to limitations of the terminals.

Carrying Capacity

The conductor size of the New York–Long Island line was selected to carry a load of 100,000 kva at 138 kv and 70 per cent loss factor* with a conductor temperature of 70 degrees centigrade and ambient temperature of 18 degrees centigrade. The thermal resistances and resistivities used were as follows:

Item	Thermal Ohms Per Foot	Thermal Resistivity in Degrees Centigrade Centimeter Units
Conductor to sheath	2.96	550
Sheath and protection	0.17	320
Oil	0.46	110
Pipe covering	0.52	215
Earth	4.97	90 (net)
Total	9.08	

Ignoring dielectric loss, the calculations gave a temperature drop from conductor to pipe, of 21.5 degrees centigrade with 400 amperes load. Laboratory tests with the same current and without voltage gave a temperature drop of 21 degrees centigrade, thus affording a partial check of the calculations. It is to be hoped that the operating companies will obtain more data on this characteristic, with special reference to the earth factor.

* Ratio of mean-square current during 24 hours to mean-square current during hour of maximum load.

Check of Adequacy of the Ovality

The ovality of the New York–Long Island cable was designed to be such that expansion of the insulation would never bring the cable to a circular cross-section with any overload likely to occur. Tests were made on cables of ovality ratios 0.90 to 0.92. Tests were made in two ways; first by creating pressure by heating and second, by applying oil pressure mechanically from an outside accumulator.

For the first test a cable of ovality ratio 0.90 inch was formed into a closed loop and placed in an oven, without external pressure, at 75 degrees centigrade and left overnight. It was then allowed to cool to room temperature. The following measurements show the change of ovality ratio.

Temperature Centigrade	Ovality Ratio Minimum/Maximum Diameter
20	0.90
75	0.93
25	0.88

For the second test, a closed loop of cable was again used but, this time, in combination with a Greer accumulator and pressure gauge. The accumulator was connected by a pipe to the conductor, and oil pressure applied in cycles of increasing magnitude, but reducing to atmospheric pressure between steps.

It will be seen from Figure 6 that the ovality ratio rose from 0.92 to a maximum of 0.97 and that an ovality ratio of less than 0.96 is reached with the highest pressure shown in Figure 9.

When the pressure was dropped to atmospheric after each application of pressure, the ovality ratio returned to its original value of 0.92.

Effect of Polyethylene on Cable Oil

Polyethylene was dissolved in cable oil at a high temperature, in various proportions, and subjected both to oxidation and electronic bombardment. Neither rate of oxidation, or deterioration under bombardment was affected by the presence of the polyethylene.

Resistance of Sheath to Oil

The cable design for the New York–Long Island line is based on a conductor temperature of 70 degrees centigrade, which was estimated to give a sheath temperature of about 53 degrees centigrade at normal load. In order to take care of overloads and give a suitable factor of safety, tests on polyethylene in oil were made at 70 degrees centigrade.

Two kinds of tests were made, one on strips of the sheath material, the other on sheathed cable.

The former tests were made on strips of polyethylene 80 mils thick by 1/4 inch wide and 2 inches long. They were immersed in open test tubes of oil and maintained at 70 degrees centigrade for one year. Measurements of thickness were made after 1, 26, 51 and 365 days. It was found that the samples immersed in oils of viscosity index 80 and higher stabilized within a month at approximately 1 per cent increase in thickness. There was no shrinkage in any direction. Oils of low viscosity index, caused a much greater swelling, 6 per cent in the case of an oil which had a viscosity index of about zero.

An oil viscosity index 80 and viscosity 33 centistokes at 210 degrees Fahrenheit was chosen for the cable insulation and one of the same viscosity index, but having a viscosity of 10.3 centistokes at 210 degrees Fahrenheit for the pipe filling.

The tests on cable sheath were made on cable of the following construction:

1. Conductor.....600,000 circular mils.
2. Paper insulation...435 mils.
3. Perforated copper shielding tape.....3 mils with intercalated paper.
4. Polyethylene sheath.....80 mils.
5. Two bronze tapes, staggered.....each 5 mils.

The ends were sealed and the cable laid in a trough of oil which was maintained at 70 degrees centigrade except for occasional reductions to room temperature.

After nine months, the cable was removed and cut open. The polyethylene was in perfect condition and had increased slightly in tensile strength.

Dielectric Strength of Cable

Due to the difficulties inherent in breakdown tests of 138-kv cables, especially where terminals must be constructed to withstand very high voltages for considerable intervals of time, tests were made on 4/0 American wire gauge cable with 220 mils of insulation, which has an oil-filled cable rating of 44 kv. This cable had a copper shielding tape and a 63-mil polyethylene sheath. The tests were made by applying a series of selected voltages as rapidly as possible and noting the times to failure. The results are shown by the solid curve of Figure 7.

Taking into account the fact that the final points on Figure 7 are "withstand" points, it appears that the cable should withstand a maximum stresss of about 1,000 volts per mil continuously at 200 pounds per square inch. The operating maximum stress of the tie line cable is 280 volts per mil calculated on the basis of the smaller diameter, so that the thickness of insulation is not determined by 60 cycle dielectric strength.

Some of the lengths of cable tested included the regular joints but no joint failure occurred.

The minimum impulse withstand voltage recommended for 138-kv cable by Halperin and Shanklin is 672-kv and that recommended by a joint AIEE–EEI* Committee, 650 kv. Tests made at Detroit by courtesy of L. I. Komives of Detroit Edison Company on samples of the New York–Long Island cable, gave an average of 765 kv. One sample included a joint but failure occurred in the cable.

The cable was made with Titebilt construction with the denser, more elastic paper outside. This was adopted here because extremely high-impulse strength was not required and load-cycle tests have demonstrated that the Titebilt construction would have more stable characteristics if required to operate under emergency conditions without pressure.

Cable of identical construction and dimensions, except that it had several layers of high specific gravity paper adjacent to the conductor, was tested at Detroit and broke down at 910 kv. Higher values are attainable with further modification of the taping and such constructions may be used where local conditions prescribe an unusually high impulse strength.

It thus appears that oil-filled cable insulation thicknesses assure a wide margin of safety for impulse strength.

Effect of Strand Shielding

A cable without strand shielding was tested and the results shown by the lower broken curve of Figure 7. The ratio of the breakdown stresses with and without conductor shielding will be found to approximate very closely to Levi-Civita's theoretical stress factor, indicating that at 200 pounds per square inch the effect of strand shielding is solely to smooth out the conductor surface, as would be expected in an ionization free cable.

Effect of Pressure on Dielectric Strength

The cable described previously with strand shielding, was tested for dielectric strength at various applied oil pressures with the following results:

Gauge Pressure Lb Per Sq In.	Maximum Stress (25 Degrees Centigrade) Volts Per Mil at Breakdown
15	750
50	1,060
100	1,270
150	1,420
200	1,510

The AEIC step procedure was followed, the final step only being considered, without reference to its duration. The maximum stress was calculated by the usual formula.

Power Factors

The following power factors are typical of those obtained on routine tests. They are approximately half the guaranteed values, which were the same as those specified by AEIC for oil-filled cable.

Temperature, Centigrade	Power Factor Per Cent
30	0.36
40	0.34
50	0.33
60	0.34
70	0.37
80	0.45

Measurements were made at 80 kv (phase voltage) and 200 pounds per square

* Edison Electric Institute.

inch, the entire cable being at the temperature stated.

Zero Sequence Impedance

Zero-sequence impedance tests were attempted on laboratory lengths, using the set-up shown in Figure 8. About all that could be learned from them was that the magnetic effect of the steel pipe was not an important factor. Laboratory tests do not check with field tests because, although the pipe is insulated along its length, it is grounded at the ends and the earth constitutes a significant part of the circuit. Zero-sequence impedance can be measured only on the line itself and it is hoped that the discussion will bring out data so obtained.

Shunt Capacitance

The measured shunt capacitance is 0.074 microfarad per 1,000 feet.

Pressure Differential

When pressure is applied to the oil in the pipe, substantially the same pressure appears almost immediately at the conductor. In other words, there is no measurable pressure differential due to the stiffness of the sheath, as occurs in the European lead-sheathed compression cables, so that low-pressure spots cannot occur when load falls off.

With rising load, however, a very considerable pressure is required to bring the ovality principle into play.

When current increases in the cable, the pressure rises until it is sufficient to alter the ovality to that corresponding to the increased loading.

The magnitude of this pressure depends on the current, its rate of rise, the thermal capacity of the cable system and the radius, within the cable, at which the pressure is measured.

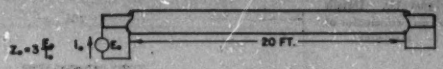
The pressure is a maximum under the following conditions.

1. Full load current applied suddenly and maintained constant.
2. Thermal capacity at minimum, with pipe in air.
3. Pressure measured at conductor.

Under these conditions, the pressure rose in seven hours to a maximum of 107 pounds per square inch over the pipe oil pressure and then, the current remaining constant, fell off to 27 pounds per square inch over the pipe oil pressure in 24 hours.

This pressure is much lower than those experienced with the European compression cables having lead sheaths.

Figure 8. Zero-sequence impedance



The rise and fall of pressure are shown in Figure 9.

Due to the fluctuating character of the load expected on this line, with its predicted 70 per cent loss factor, and the greater thermal capacity of a buried pipe system, it is expected that the pressure differential will seldom reach the maximum attained in this test.

When a cable is heated under 200 pounds per square inch pressure by current, to 70 degrees centigrade conductor temperature, and the external pressure released to atmospheric, at the rate of five minutes for each 25-pound change, the pressure at the conductor falls as follows in relation to the external pressure.

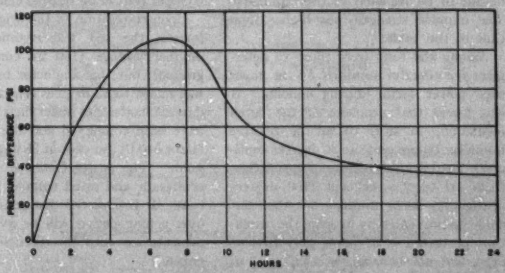
External, Lb Per Sq In.	Internal, Lb Per Sq In.	Difference, Lb Per Sq In.
200	208	8
175	187	12
150	165	15
100	123	23
50	78	28
0	55	55

The final internal pressure of 35 pounds per square inch dropped to 25 pounds per square inch in three hours and remained at this value for at least two more hours.

Amount of Pipe Oil Expansion Under Load

An experimental length 40 foot long, of pipe and cable similar to that used on the New York–Long Island line was installed in the laboratory. The conductors were Y-connected for current loading and the pipe sealed except for means to move oil in and out, at the ends.

Figure 9. Pressure differential between cable and pipe oil with constant load



The pipe was covered with air-cell insulation to give a total thermal resistance equal to that calculated for a buried pipe.

Constant pressure of 200 pounds per square inch was maintained during load period and the expansion determined by letting out the expanding oil. Conductor temperature was measured by copper resistance; pipe temperature and ambient, by thermocouple.

The total oil expansion for a normally loaded cable from no load to full load (47 degrees centigrade rise) was 3.22 cubic feet per 1,000 feet of pipe over a 20 hour period.

Tests were also made with conductor temperature raised to 93 degrees centigrade, in which case the total oil expansion in 20 hours was 4.54 cubic feet per 1,000 feet.

These tests determined the necessary oil reservoir and pumping capacities for normal and overload operation.

Rate of Oil Expansion in Pipe

In the course of the tests described before, the maximum rate of flow of oil was determined.

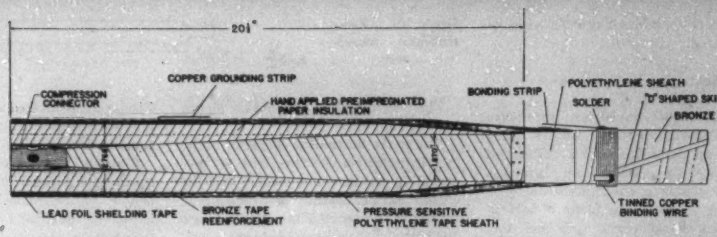
It was found to be 10 cubic centimeters per foot per hour or 0.000168 cubic inch per foot per second during the first three hours with normal loading and 13.3 cubic centimeters per foot per hour or 0.000224 cubic inch per foot per second with loading to a conductor temperature of 93 degrees centigrade.

Assuming 10 cubic centimeters per hour per foot of pipe, a rate of 3750 cubic centimeters per minute is obtained as the average rate of flow for 45,000 feet.

Pressure Drop in Pipe

The pressure drop at these rates of flow was determined with oils of various vis-

Figure 10. Details of one conductor of the joint



cosities. The drops were found to agree very well with Poiseuille's formula for viscous flow in pipes provided that, for oils of suitable viscosity, the effective diameter of pipe is taken as about 70 per cent of the equivalent diameter of the cross-section of oil, that is pipe minus cables. Using Poiseuille's formula with this correction and assuming a pressure rise of 100 pounds per square inch at the minimum expected operating temperature of the cable, 40 degrees Fahrenheit (4.5 degrees centigrade), and assuming feed from one end of the line, it was found that the oil should have a viscosity of not over 1,000 centistokes at 40 degrees Fahrenheit. This order of viscosity was obtained by mixing the cable oil with an oil of the same electrical quality but of lower viscosity, the mixture having the same viscosity index as the cable oil. A low viscosity is desirable from the standpoint of avoiding excessive pressure drop in the pipe during conditions of maximum flow However, if for any reason the pipe oil should mix with the cable oil it is advantageous to have the viscosity high and to have the oil compatible electrically with the cable impregnant. The pipe oil viscosity was selected as a compromise between these conflicting requirements.

Splices

A series of tests bearing on development and operation of splices, including impulse tests were also made, but cover too many details to be included in this summary. The impulse strength was higher than that of the cable.

Among the tests were those to determine the effect of humidity on the paper tape power factor during splicing. It was found that exposure of the bared insulation, in situ, to air of relative humidity 50 per cent, at 30 degrees centigrade caused a rise of tape power factor, from 1.3 to 2.2 per cent (100 degrees centigrade) in 25 minutes, but that this could be prevented by keeping the insulation protected with plain polyethylene tape, and not basting.

Basting with fresh oil at 120 degrees centigrade had the same initial effect, but the polyethylene tape protection was preferred because of danger of the basting oil becoming oxidized from continuous overheating and handling. Tests showed that continued basting with the same oil resulted in a rise of tape power factor. Basting was rejected also because the hot oil removed cool surface oil and replaced it with hot oil which would contract into the paper upon cooling.

Load Cycle Tests

Preliminary load cycle tests were made on single cables in brass pipes in order to obtain data for designs and then to check designs.

Some of these tests were made with voltage and current and others with current only. In some cases joints were included in the tests.

Tests on a 90 foot replica of the tie line, that is, with three cables in a steel pipe, were started at 25 per cent over rated voltage with current to bring the copper to 70 degrees centigrade. Due to electrical failure of a transformer, it was necessary to reduce the voltage to three-quarters the rated value and continue the test on this basis through 49 load cycles of 48 hours each, with current on for half this period. The power factor at 70 degrees centigrade and rated voltage remained unchanged at 0.31 per cent and the final ionization factor (11 kv to 100 kv) was 0.04 per cent at 28 degrees centigrade.

Upon restoration of the original transformer, the test was resumed at the original voltage (100 kv conductor to ground), but the conductor temperature was raised to 85 degrees centigrade. To date, 21 load cycles under these conditions have been completed and the ionization factor is 0.01 per cent at 28 degrees centigrade. The power factor at 70 degrees centigrade and rated voltage, is 0 34 per cent. It is expected that results of tests over a long period will be available for presentation with the discussion of this paper.

Accessories of 138,000 Volt Compression Pipe Type Cable*

The very simple accessories of the European gas-pressure compression cables are here replaced by the more elaborate pressure control equipment necessitated by the use of oil as the pressure medium. In the design of the accessories, the experience of other pressure systems in this country and abroad was given consideration. Starting with this as a basis, the details of design were developed by Phelps Dodge engineers in collaboration with the company's research laboratory, to fit the requirements of this installation.

The cables, pipes, manholes and installation equipment are covered by other papers of this symposium. This leaves only the cable joints, terminals and pressure regulating equipment to be considered under the heading of accessories.

Cable Joints—Normal and Semistop

Figure 10 shows details of a single conductor of the splice (either normal or semistop).

Compression-type connectors are used, with the indentations rounded out with solder applied with a soldering iron.

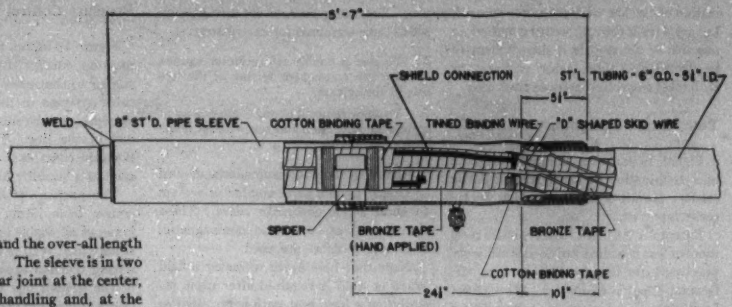
The insulation consists of hand-applied pre-impregnated paper tape applied butted. It is of the same grade as the cable insulation.

The polyethylene sheath is carried across the joint by the use of pressure-sensitive polyethylene tape which is applied half-lapped to a minimum thickness of 100 mils, forming a seal from one end of the joint to the other. This also permits grounding of the cable and joint shielding at every splice by zigzagging a thin copper strip through the tape wall and bonding it to the skid wires and armor.

Figure 11 shows the normal joint. The sleeve consisted of eight-inch outside

* J. H. Palmer and W. A. DelMar.

Figure 11. Normal joint



diameter steel pipe and the over-all length was 5 feet 10 inches. The sleeve is in two parts with an annular joint at the center, in order to aid in handling and, at the same time, to allow centering of the splice in the manhole. Welded joints having long heat paths from the weld to the pipe were used for all welds made after the cable was pulled in, to protect the cable from the heat of welding.

Figure 12 shows a semistop joint. The cable splices are the same as for the normal joint, the difference lying in the semistop feature at each end, consisting of glands or stuffing boxes. The packing for these glands is composed of a soft oil-resistant synthetic rubber compound which has been thoroughly tested to prove that it neither affects or is affected by the pipe oil. The varnished cambric build-up under these glands serves a double purpose; permitting a build-up to a close fit without having to make the gland too tight for easy assembly and at the same time distributing the area of transition between a sheath which "breathes" due to thermal expansion and contraction of the cable, and the section of this sheath which is bound down by the gland and not permitted to "breathe."

The sleeve is divided into four parts both because it is necessary for assembly and for ease in handling. The main sleeve is 10-inch outside diameter steel pipe and the over-all length of the joint is slightly over 10 feet.

Safetyheads are used as in other pipe installations. They are set for 500 pounds per square inch and are arranged to relieve either the pressure in the joint, or in either section of pipe connected to the joint, in case of electrical failure. All three safetyheads discharge into an expansion tank which contains a drain valve. If this valve is opened and oil or gas is discharged, a failure of one of the three safety heads is indicated and sectionalizing to isolate the cause may be done by proper manipulation of the valves.

Figure 13 shows a detail of the single conductor cable where it passes through its semistop gland as mentioned before. These semistops follow previous practice, with the exception that the gland makes contact with the oval polyethylene sheath. They are intended for sectional-

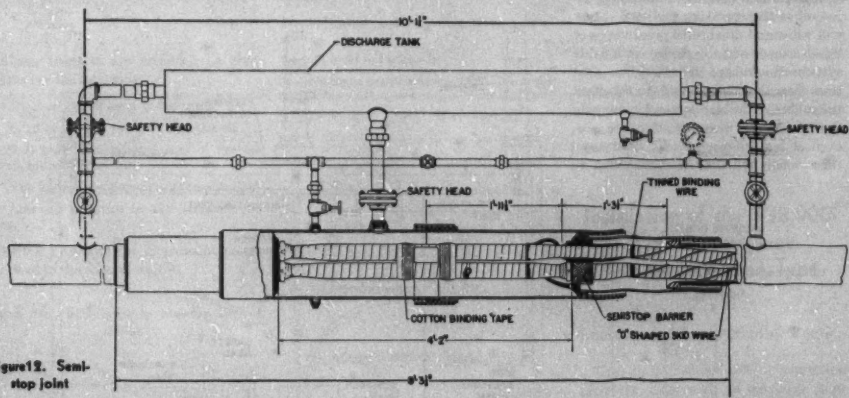


Figure 12. Semistop joint

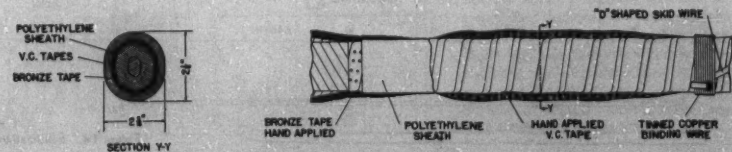


Figure 13. Semistop gland

ization of the line with pressure removed, but tests with the full pressure applied to one side of the stop have shown them to be surprisingly tight either with gas or with oil as respective pressure mediums.

Termination Equipment

Figure 14 shows a schematic diagram of a termination of the system and the relative locations of the various accessories involved.

Except for the base, the terminals are of more or less conventional design and were purchased for this installation from the General Electric Company. They differ from other high-pressure terminals of American design in that the base contains a gland or stuffing box which seals to the cable sheath and coverings, forming a barrier between the pipe and the pressure or stop tube of the terminal This allows the use of cable compound in the terminal stop tube where the compound is in contact with the cable insulation. Pressure is maintained here by connection to the oil in the pipe through two compensators which transmit pressure through oil-resistant rubber bladders, assuring complete separation of the pipe oil from the terminal stop tubes, and substantially equal pressures in the pipe and tubes.

The compensators were manufactured by Greer Hydraulics, Incorporated and were originally built for use with hydraulically-operated devices on airplanes, using oil as the operating medium. They will withstand differential pressures over 2,000 pounds and for service with cable systems should have unlimited life. Our tests show that the oils used do not affect the rubber in the bladders and conversely that the rubber does not affect the oils, even at temperatures much higher than those which will ever be encountered in use. Two compensators are used at each set of three terminals for two reasons:

1. To give a double safety factor against leakage and consequent mixing of the two oils in the system.

2. To allow removal of one compensator at a time for inspection and test without interruption of service.

Figure 15 shows the spreaderhead used to change from a three-conductor system to three single conductor risers. These risers must, of course, be nonmagnetic; thus copper tubes are used.

Note that here again wherever a field braze or weld is required after cable installation a long heat path is provided so that no damage will be done to the polyethylene sheath by the heat. Tests have shown even this to be unnecessary, however, due to the high thermal capacities and conductances involved, and due to the skid wires which space the cable quite effectively from the pipe. The safetyhead is provided to relieve the pressure if oil surges should occur in the pipe. If ruptured, oil would be discharged into the relief tank. This would also serve as a means of locating a cable failure, in the event of failure causing an oil surge. The section in which failure has occurred would be found by opening the drain valves on the relief tanks both at the spreaderheads and at the semistop joints to see if the safetyheads have ruptured.

Pressure Control Station

Figure 16 shows the equipment in the pressure control station. This is quite similar to that used for other oil-pressure cable systems in this country. On this system, one such station is located at each end of the line. The line will operate from the controls at either end, one being used as a standby for the other.

The purpose of this equipment is, of course, to maintain the pipe oil pressure between allowable limits (200 pounds per square inch ± 20 pounds per square inch) during thermal expansion or contraction of the system due to load and ambient temperature changes. This is accomplished as follows:

1. If the pipe pressure falls to 180 pounds per square inch, due to thermal contraction, one of the pressure control switches will close, starting the oil pump. When the pump brings the pressure to 200 pounds per square inch, the same switch will open, stopping the pump. If the pressure increases to 220 pounds per square inch, due to thermal expansion, one relief valve will start passing oil back to the reservoirs, thus dropping the pressure. If this valve for any reason fails to relieve the pressure rise, a second one in parallel with it will operate. The tanks provide reservoirs for this necessary surplus oil. The oil is stored in these tanks under an atmosphere of dry nitrogen to prevent oxidation. Nitrogen pressure is maintained between 4 and 15 pounds per square inch by conventional gas pressure regulators and a 220 cubic foot gas tank.

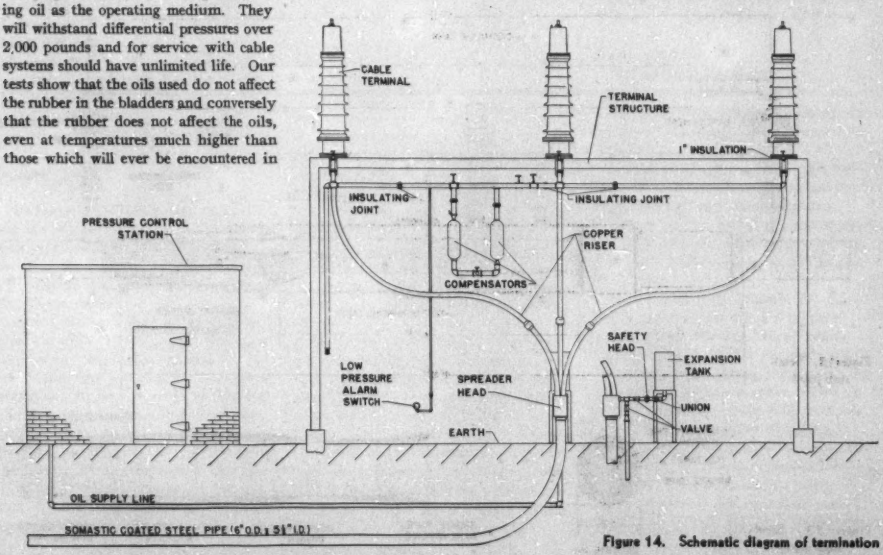


Figure 14. Schematic diagram of termination

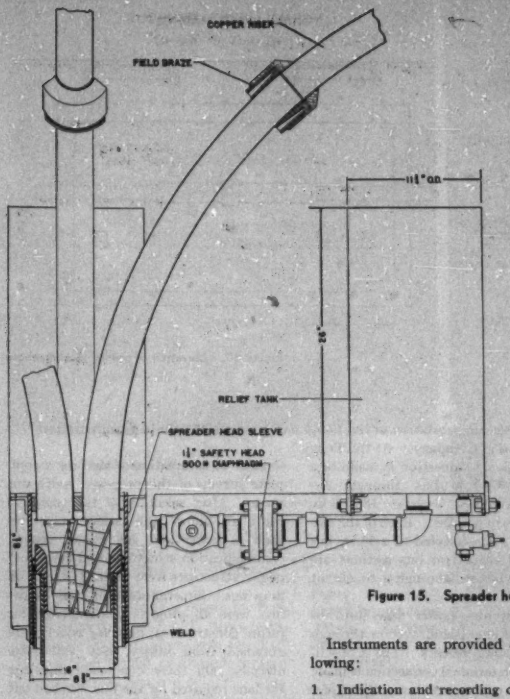


Figure 15. Spreader head

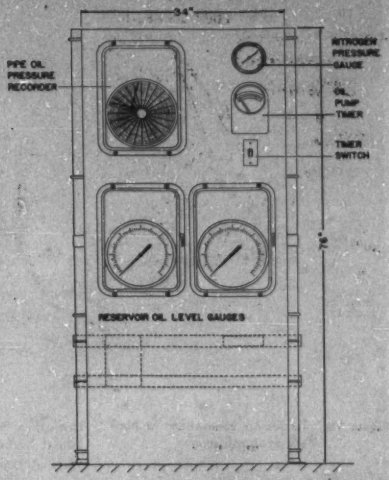


Figure 17. Instrument panel in control station

Alarm contacts are provided to give alarms for the following:

1. High or low oil level in the tanks.
2. High or low gas pressure over the oil.
3. Too long time of operation of the oil pump, indicating a leak.
4. Too high or low pressure in the pipe line.
5. Low oil pressure in the terminal oil lines.

Figure 17 shows the instrument panel included in the control station.

Instruments are provided for the following:

1. Indication and recording of pipe pressures.
2. Oil level in tanks.
3. Nitrogen gas pressure over the oil in the tanks.
4. Indication of time of oil pump motor operation with alarm contacts for excessive time.

All of the foregoing equipment shown in Figures 16 and 17, including the oil tanks, is enclosed in a house in which the temperature is maintained at 65 degrees Fahrenheit or higher by an electric space heater.

This assures fast and accurate operation of the controlling and indicating instruments which might be slowed down or even put out of operation if the oil in these small lines were left at ambient temperature in the winter.

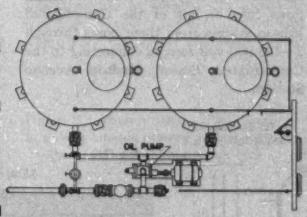
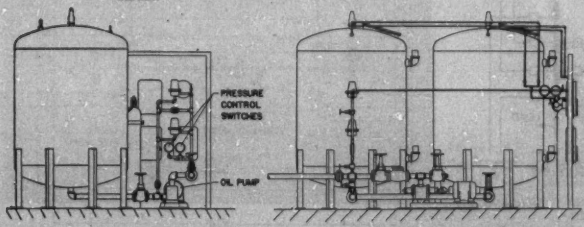


Figure 16. Equipment in pressure control station



Installation of the 138,000-Volt Polyethylene Compression Pipe-Type Cable*

Location and Description of Route

The first 138,000-volt compression pipe-type cable with an extruded polyethylene sheath is installed in a line between the Jamaica substation of the Consolidated Edison Company of New York, Inc. and the Valley Stream substation of the Long Island Lighting Company, a total distance of approximately 8.2 miles of which 5.7 miles is in the Con-

* C. T. Hatcher and E. F. DeTurk.

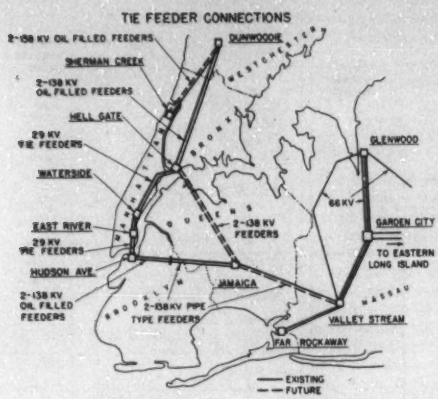


Figure 18. Tie feeder connections in New York metropolitan area

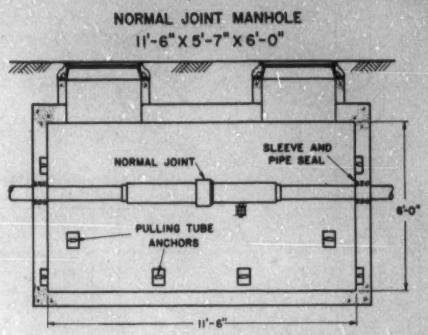


Figure 20. Elevation of normal joint manhole

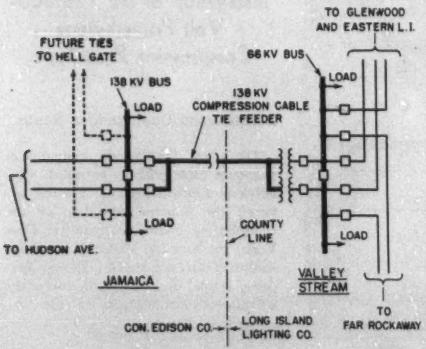
solidated Edison territory and 2.5 miles in Long Island territory. The line will be used for interchange of power between the two systems depending upon the requirements in each system from time to time.

The route of the line lies wholly within city areas and traverses heavy traffic areas through paved streets. This tie line in relation to other tie lines in the New York metropolitan area is shown in Figure 18.

Figure 19 shows diagramatically the connections at the Jamaica substation of the Consolidated Edison Company and the Valley Stream substation of the Long Island Lighting Company. At the Long Island terminal, connection is made to a double 66,000-volt bus through 2—30,000/50,000 kva 3-phase 138/66-kv transformers in parallel. Each of the two transformers is connected to a 66-kv bus section and these two bus sections are normally connected through a tie circuit breaker.

From this bus, power flows into or out of the Long Island system through numerous 66-kv feeders. The Consolidated Edison terminal connection is made to either the east or west bus sections of a double 138,000-volt bus with an oil circuit breaker between the cable and each section of bus. The buses are tied together through a tie oil circuit breaker. Power flows into or out of the Consolidated Edison system from these buses through 2—138,000-volt feeders connecting to the Consolidated Edison Hudson Avenue Station.

Figure 19. Consolidated Edison System—Long Island Lighting Company interconnection



Pipe and Manhole Construction

Prior to installation of the pipe a complete survey of the proposed route was made. After selection of the tentative route all known sub-surface structures such as water and gas mains, sewers, communication structures and company-owned structures were plotted to scale on drawings. Superimposed on these drawings were all projects contemplated for future construction, data for which were obtained from utility, city and state officials. On these composite drawings the lane required for the line was laid out so as to avoid existing and future sub-surface structures.

The pipe used was six-inch outside diameter seamless steel tubing having a 1/4 inch wall thickness. The pipe was cleaned externally and internally at a central cleaning and coating plant. After cleaning, the inside of the pipe was coated

Figure 21. Elevation of semistop joint manhole

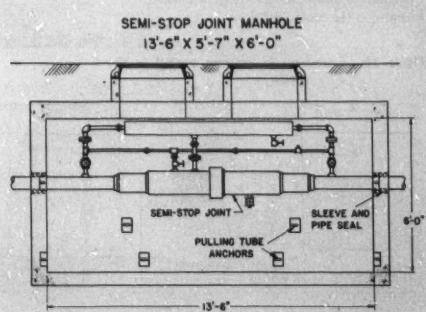


Figure 22. Installation of precast manhole

Figure 24. "A" frames mounted on wheels facilitate placing pipes in trench

with a lacquer made by the Dearborn Chemical Company. The outside of the pipe was coated with 1/2 inch of Somastic—a mixture of asphalt, limestone, sand, and asbestos fibre.

In order to provide for future increase in tie capacity two 6-inch pipes were installed. The two pipes were installed in a common trench on 18-inch centers and to a nominal depth of 4.5 feet. This spacing was selected after calculations had been made of the heat dissipation when both lines were fully loaded.

A study of allowable pulling stresses in the cable indicated that straight sections should be held to approximately 2,000 feet. In those sections where it was necessary to include offsets or bends, either horizontal or vertical, pulling tensions were calculated and the section length was chosen so as not to exceed a tension of 8,500 pounds per square inch (approximately 7,000 pounds total pull) in the copper or a maximum length of 2,000 ft., whichever proved to be limiting. Manholes were used for splicing the cables. Consideration was given in the Long Island section to burying the splices to eliminate the cost of permanent manholes. Temporary collapsible manholes would have been used during splicing and removed after completion of the cable joints and the entire opening filled in and paved over as for the remainder of the cable trench. Permanent manholes were finally decided upon because the route lay along the middle of a heavily traveled street

Figure 23 (below left). "Pig" used in making internal pressure test of pipe welds at 500 pounds per square inch

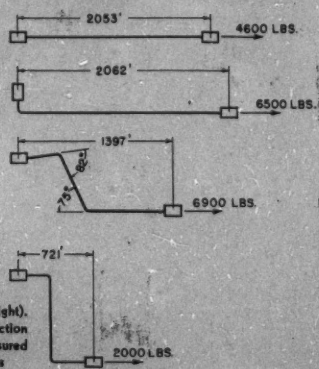


Figure 25 (right). Representative section lengths and measured pulling tensions

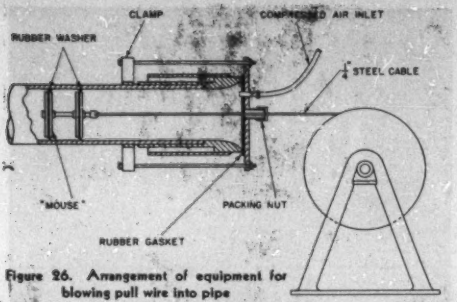


Figure 26. Arrangement of equipment for blowing pull wire into pipe

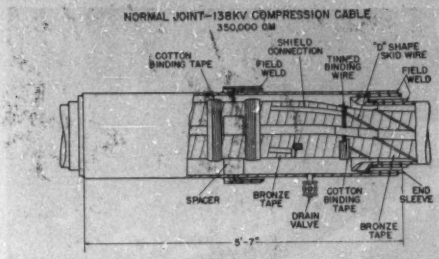


Figure 28. Internal arrangement of normal joint

with concrete paving. A second manhole excavation would otherwise have been required if a second cable were installed, and this would be expensive and objectionable to the civic authorities.

The manholes were of precast concrete construction that is, they were poured at a central plant and transported to the field for installation. In the New York section of the line semistop and normal joint manholes were used and in the Long Island section only normal joint manholes were used. Figures 20 and 21 show the normal and semistop joint manholes. Two roof openings were used to facilitate installation of the cable. Oversized pipe sleeves were cast into the end walls so as to provide flexibility for installing the pipes in the manholes. Figure 22 shows a precast manhole wall section being lowered into the street opening.

The pipe was delivered in approximately 40-feet double random lengths. Upon delivery of the pipe to the street location the random lengths were arc welded together above the trench wherever possible. The pipe ends were flared for use with chill rings. Welds were made in three passes and each welder was required to make qualifying welds. Upon completion of a weld it was tested internally with 500 pounds dry nitrogen gas and with the outside of the weld surrounded by a trough of water so that leaks could be detected. The gas pressure testing was greatly expedited and the amount of gas kept to a minimum by the use of a test "pig." This device is about 30 inches long and consists of two steel reinforced rubber "tires" connected together by a steel linkage in the center. This "pig" was inserted in the ends of the pipes to be welded together, so that the weld was over its center. After the weld was made the "tires" were inflated with 600 pounds nitrogen pressure, and the pipe section between the two "tires" and including the weld was then tested at 500 pounds pressure as described before. Gas was supplied through two hoses leading out the open end of the pipe line. When the test was completed, the gas pressure was released and the "pig" pulled out the open end of the pipe by means of a pulling line. This test pig is not new for this work and is believed to have first been used in this country on the recent New Orleans pipe cable installation. Figure 23 shows the "test pig."

After several lengths of pipe were welded and tested they were suspended over the trench from chain hoists attached to "A" frames and were then lowered into the trench or pulled into the trench under structures. Figure 24 shows the A frame method of lowering the pipe. The welded sections of pipe after being placed in the trench were welded together in place until a section between manholes had been completed.

The route of the cable crosses several

Figure 27. Cables are fed into feeding tube over large diameter sheave

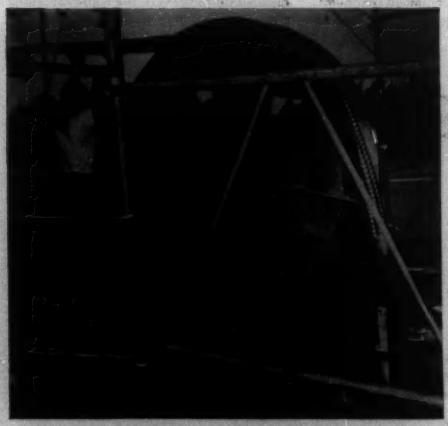
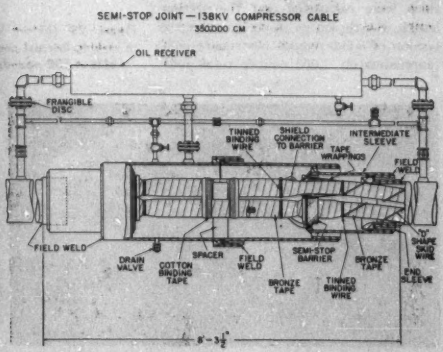


Figure 29. Internal arrangement of semistop joint



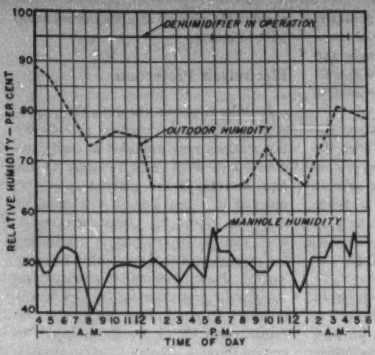


Figure 30 (left). Typical section of controlled humidity chart

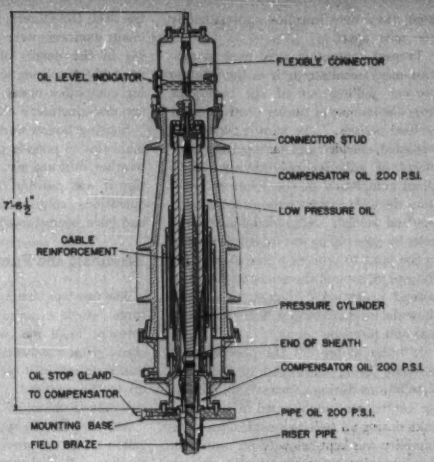


Figure 31. Sectional view of 161-kv terminal for compression cable

brooks, several drainage culverts and a railroad track. At each of those locations, the cable pipe was laid inside of a ten-inch cast-iron pipe sleeve. In the case of certain drainage culverts, the pipe was cut directly through the culvert opening in order to avoid the deep pits which would have been necessary to pass beneath the bottoms of the culverts. A large part of the route in the Long Island section is below the normal water table and considerable pumping and stream by-passing was necessary to allow satisfactory pipe installation. Upon completion of a section between manholes and after some back-filling had been completed the pipe was subjected to an internal air pressure of 500 pounds and a pressure drop test was made. The air pressure was provided by a special compressor capable of supplying 500 pounds pressure and equipped to remove moisture from the air. Prior to raising the pressure to 500 pounds the pipe was flushed with dry air from the compressor until a maximum dew point of −30 degrees centigrade was reached at both ends of the pipe.

In order to prevent damage to the pipe coating a bedding of clean sand free from stones or similar objects was placed in the bottom of the trench. A cushion of clean sand was also placed around and over the pipes before the remaining fill was bulldozed into the trench.

The Somastic coating was tested electrically upon receipt at the central receiving yards and again before the trench was back-filled, so as to determine if the coating was free of voids or other damage which might cause future corrosion troubles. The welds were also coated with Somastic by means of a mold and were tested electrically to determine the quality of the coating. Insulation resistance measurements were made on the pipe after back-filling; and, a value above 10,000 ohms per 1,000 foot of pipe had to be obtained before the section was considered satisfactory. Where sections of the coated pipe failed on the resistance test, an interrupted d-c potential was impressed between the pipe and ground and the defect in the coating was located by a survey over the pipe from the street surface. Where breaks in the coating were detected by this method the pipe was exposed and the coating repaired.

Bends were avoided wherever possible, but the route which was followed necessitated both horizontal bends and vertical dips in some sections between manholes. In the majority of cases it was possible to avoid having more than one ninety degree bend in a section; however, in one section this was unavoidable and two ninety degree bends were installed. Examples of typical sections and the actual pulling tensions measured in these sections are shown in Figure 25. Complete records were kept of pulling tensions for all cable sections. The average coefficient of friction for straight sections including minor offsets and vertical dips was found to be 0.242.

Cable Installation

Prior to the installation of the cable it was necessary to install the pipe a small steel line for drawing the cable pulling steel winch line into the pipe. This was accomplished by blowing a "mouse" through the pipe by means of compressed air. The mouse is a disk arrangement to which was connected a 1/4-inch steel line. Figure 26 shows the arrangement of equipment for this operation.

The three single-conductor cables were pulled into the pipe at the same time. Special pulling eyes were installed on each cable at the factory prior to shipment. Due to the use of two manhole entrances it was possible to use a pulling-in tube and a pulling-out tube constructed with rollers for the cable installation. These tubes were coupled onto the pipe during pulling operations.

The 3-cable reels were placed in tandem at one manhole and a special cable pulling winch was used at the other manhole location. The cable was fed directly from the reels over a special sheave and into the pulling-in tube. Approximately five gallons of the cable impregnating oil was fed into the pipe for lubrication during each pull. Figure 27 shows the three cables being pulled into the pipe. After the cables were installed in the pipe, a pipe seal or "night cap" was placed over the cable ends and fastened to the pipe. A positive nitrogen pressure of approximately five pounds was then maintained in the pipe until splicing was begun.

Cable Splices

Both normal and semistop joints were used in the Consolidated Edison section of the line whereas only normal joints were used in the Long Island Lighting section. Where semistop joints were

used, they were installed approximately one mile apart.

Pressed connectors were used to provide mechanical strength in the joint to prevent pulling out of the conductor from the connector during contraction at no-load periods. The joint is paper tape insulated, shielded and has a polyethylene jacket of adhesive polyethylene tapes. Upon completion of the jointing operations the joint casings were placed in position and welded. At semistop joint manholes by-pass piping was installed external to the joint to provide a free passage for the pipe oil around the semistop glands in normal operation. Figures 28 and 29 show the internal arrangement of the normal and semistop joints.

In order to prevent the possibility of moisture being entrapped in the joint installations during construction, humidity control was maintained in the manholes during all splicing operations. The humidity was kept below 55 per cent relative humidity by means of silica gel air conditioners and by bleeding of cool dry air from the spare pipe into the manhole. Two of the manholes in the Long Island section extended about three feet below the water table, and in order to maintain satisfactory humidity conditions it was necessary to line those manholes with a waterproof cellotex lining, with an air space between the lining and the manhole wall. In addition, there was the usual drying and dehumidifying procedure. A typical chart of controlled conditions in a manhole as compared to outside humidity is shown in Figure 30.

Before any joints were constructed in the field, the splicers, foremen and supervising engineers were given careful training in the details of construction and assembly at splicing schools of the operating companies under the supervision of the manufacturer's engineers.

Splicing houses were installed over the manholes to provide protection from the weather, dust and dirt. With this protection it was possible to continue splicing operations continuously after a splice had once been started.

Terminals and Pumping Plant

The line was terminated at each end in three single conductor potheads. A spreader head was welded to the pipe above ground and each of the three cables was installed in a separate copper pipe up to the pothead. The pothead construction is shown in Figure 31.

An oil pumping plant and two 750-gallon tanks are provided at each end of the line. A dual pumping plant at one end of the line or a pumping plant at either end was considered satisfactory and in this installation it was decided to install one at each end. Figure 32 shows the terminal arrangement and pump house at Valley Stream.

Installation of Oil

Upon completion of the joints and the terminals the entire line was evacuated until satisfactory vacuum had been obtained. Oil was transported by tank truck from the manufacturer's plant to Valley Stream where it was pumped into the line. During the filling operation vacuum pumps were operated at the Jamaica end of the line and a check was made of the progress of the oil along the route of the pipe by sampling at semistop joint locations.

Electrolysis Protection

In order to permit checking the quality of the outside pipe coating periodically test stations were provided at each manhole. Test leads were fastened to the pipe outside of the manhole and terminated in a test box in the manhole shown in Figure 33. Insulation resistance can be determined by measuring the resistance of the coating for a known length. Figure 34 shows this connection.

Cathodic protection for the pipe line is provided by connecting the pipe at the Jamaica and Valley Stream terminals to ground through a 2-volt 23-plate grounding battery. With the negative terminal of the battery connected to the pipe, the pipe line is maintained at a negative potential at all points a long the line. Tests made on the grounding battery indicate that in the event of a feeder failure, the battery will carry 15,000 amperes for one second without injury.

Thermal Measurements

In order to check temperature conditions on the line and to obtain data for soil resistivity calculations, the following facilities were provided. At each manhole location in Consolidated Edison territory and at two locations in Long Island territory a thermocouple was attached to each pipe, as shown in Figure 33 and the thermocouple leads were terminated in the manhole. Also at a selected number of locations along the line, thermocouples were installed to measure soil ambient temperature. These thermocouples, placed at the same depth as the pipe, were located at points laterally opposite the pipe thermocouples and at a distance from the pipe sufficient to avoid the effect of

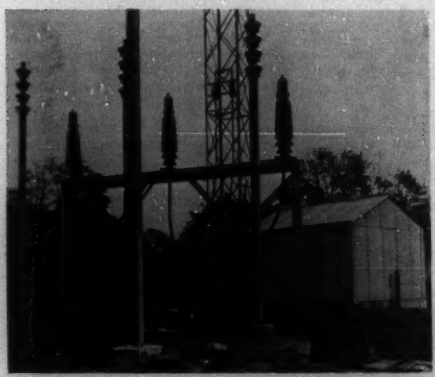
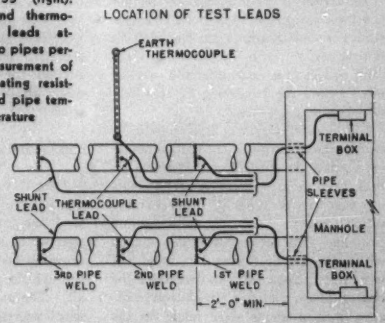


Figure 32. Line terminals and pump house at Valley Stream

Figure 33 (right). Shunt and thermocouple leads attached to pipes permit measurement of pipe coating resistance and pipe temperature



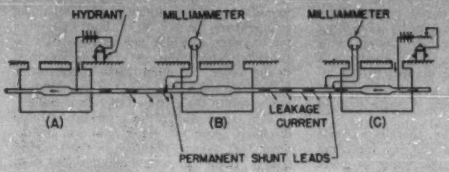


Figure 34 (above). Electrical connections for measuring pipe coating resistance

1. Apply d-c potential at (A)
2. Apply d-c potential at (C) and adjust to obtain zero current on millimeter at (C)
3. Milliammeter at (B) reads leakage through coating from (B) to (C)

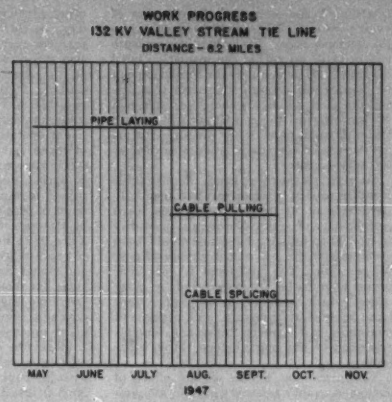


Figure 35. Progress of installation work

heat dissipated from the pipe. It will therefore be possible to measure the pipe operating temperature under known loading conditions, and from the pipe temperature rise above soil ambient, determine soil resistivity values. In addition to the pipe thermocouple installations, test heater cells have been installed in the vicinity of the soil thermocouples for the purpose of obtaining comparative data on seasonal variation of soil resistivity. It is planned to use such heater cells in the future to explore soil resistivity variation along proposed feeder routes and for other tests in connection with soil resistivity.

Progress of Work

An idea of the time required for the various processes described is given in Figure 35.

Installation of Pipe . . . May 12 to September 5, 1947
Installation of Cable . . . July 30 to September 30, 1947
Installation of Joints . . August 11 to October 10, 1947

Conclusion

From the foregoing it will be seen that new developments are taking place in the field of underground high-voltage power transmission. While all precautions were observed to protect the pipe and cable from contamination by dirt and moisture throughout the installation, the continuous plastic cable sheath provides an additional factor of safety for the cable insulation. The complete integrity of the insulation as produced under controlled factory conditions is maintained from the time the cable leaves the factory until the ends are spliced while the disadvantages of sheath losses and possible sheath ruptures inherent in previous lead-sheathed compression-pipe type systems have been eliminated.

The authors wish to express appreciation for assistance in compiling data for this paper to R. W. Gillette and F. P. West of the Consolidated Edison Company of New York, Incorporated, and to J. L. Davidson of the Long Island Lighting Company and to the electrical engineering and construction staffs of Ebasco Services Incorporated who laid out and supervised construction of the Long Island Lighting Company's section of the line.

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7. Evolution of the Supertension Cable, **T. R. Scott.** *Electrical Engineer*, (London, England), volume 9, March 1, 1940, pages 610–614.

Discussion

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): There are a number of points in this paper which warrant careful consideration. It is noted from Figure 6 of the paper that the internal oil pressure rises from zero to 200 pounds per square inch, when the "ovality ratio" (ratio of short diameter to long diameter) changes from its initial value of 92 to 97 per cent. However, calculation indicates that this change in shape can provide for less than 1/4 per cent increase in volume. Even if only 40 per cent of the cable is occupied by oil, this would seem to mean that this change of shape can accommodate only about 8 degrees centigrade change of cable temperature.

These data from Figure 6 should be compared with the data shown in Figure 9 of the paper which, for an unstated ampere load on the cable, show only 107 pounds per square inch increase of pressure and show that this ultimately falls off very greatly.

If the cable of Figure 9 was initially not well saturated or if the system contained, for instance in the pipes or the gauges, air or gas pockets, a condition such as shown in Figure 9 could be expected. That is, the pressure would be much less initially than as found in Figure 6 and would fall off as the gas goes into solution. It will be interesting to have the author's explanation of the seeming discrepancy between the data of Figure 9 and that of Figure 6.

DelMar also cites a test where a closed loop of cable was heated in an oven from 20 to 75 degrees centigrade. The ovality ratio changed only from 0.90 to 0.93. This change of shape corresponds to a volume increase of only about 0.2 per cent. However, it can readily be shown that, in the absence of voids, the volumetric change of the cable structure, largely due to the oil, is around 1.5 per cent for this temperature range. The paper does not make clear how the difference of 1.3 per cent is accommodated.

If we assume for discussion that the cable does not contain within its "seal," this initial "void" of 1.3 per cent of its volume, then this "void" presumably would be produced thereafter by a permanent alteration of the sheath or its reinforcing tapes.

On the assumption that there is an initial void space of 1.3 per cent at room temperature, this space would be increased to about 2 per cent if the cable were cooled to zero centigrade, as a result of the further con-

traction of the saturant. A reduction of ovality ratio to 0.75 would be required to fill this void space. No hydrostatic pressure can be built up within the insulation until it is filled. Hence, unless the insulation can be compressed enough to permit this distortion, is it not likely that the cable will not be a pressure cable at all but will contain ionizable voids at very low pressure, just as does any solid-type cable when operating at low temperature?

Suppose, on the other hand, that the cable Del Mar used in this loop test had been fully saturated at 20 degrees centigrade, then calculation shows that the change in ovality from 0.90 to full circular shape would have accommodated a volumetric change of only 0.4 per cent. The additional 1.1 per cent would have to be accommodated by other means which would seem to require very large hydrostatic pressure, and permanent stretching of the metal tapes and possible extrusion of the polyethylene through the tapes.

The discussion herein of the effect of change of ovality is based upon the assumption of an elliptic cross section of the sheath. It is believed that no important error results from this assumption even when the conductor is much flatter than an ellipse having the same major and minor diameters. Furthermore, a factor has been omitted from this calculation which, if taken into account, would appear to reduce still further the accommodation indicated for change of ovality ratio. This factor is the expansion with temperature of the polyethylene which takes away directly the same amount of possible accommodation for oil expansion.

I would like to have the comments of the authors concerning the possibility that the polyethylene would creep circumferentially around the cable due to the differential mechanical pressures. When the internal temperature is high, the cable is being forced toward a more nearly round cross section by pressure of the sheath on the long axis. The polyethylene adjacent to the long axis is then under large mechanical pressure whereas that on the flat side is under little or no pressure. With the polyethylene at the maximum operating temperature, and thus with minimum resistance to flow, would there not be a circumferential flow, resulting in a thinning of the polyethylene wall at the ends of the oval? The tendency to flow will be in the opposite direction at low temperature, but because of the much greater resistance of the polyethylene to flow at low temperatures, would not the tendency for the polyethylene to flow away from the ends be cumulative?

We do not believe that the foreign operating experience of lead sheathed compression cable is necessarily a valid answer to the foregoing questions or those questions raised by Pettee and others, because the characteristics of polyethylene and lead are inherently different.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): Twenty years ago, the AIEE had presented to it a symposium of papers covering the initial commercial installations of oil-filled underground power cable. These installations symbolized cable's coming of age and they were based on developments originated by the Societa Italiana Pirelli. This and the other pipe-type cable paper presented today, together with work done by some other manufacturers on extra high-voltage cable systems in this country, reflect great credit on the American cable manufacturing industry. As in this case, the encouragement of cable users have been of some help in such developments.

For operation at 69 kv, there is a choice of many types of cable systems, including solid-type or oil-filled cable either in ducts or buried, medium-gas-pressure or self-contained high-pressure cable, and an assortment of pipe types. Altogether, there may be 15 types at 69 kv, about 12 types at 138 kv, and an unknown, but smaller number, of types at 230 kv. My point now is that one should not become enamored of any one type, but should approach each situation on a straight engineering basis. As experience develops, however, perhaps a few of the many types will drop out of the picture.

Of course, the simplest installation is of solid-type cable with sealed joints filled with compound and having no oil reservoirs. Such an installation of 69-kv cable was made in ducts in Chicago over five years ago and a similar, but buried, installation is to be made in Winnipeg this year.

Also, a line of low-pressure oil-filled cable should require very little maintenance according to operating experience of recent years. In this connection, there is a psychological problem, since the joints, reservoirs, relays, and so forth in manholes and at terminal structures may be readily inspected and so this is done frequently in some cases; for a pipe-type cable installation, not much can be given periodic inspection and so not much is done. A similar experience occurred when the practice was changed from the use of gasketed covers to welded covers on some liquid-filled reactors and transformers.

In the pipe-type cable installations, there is the possibility of between-phase failures. This means a higher interrupting duty for the existing or required new circuit breakers, or both, than for installations for single-conductor solid-type or oil-filled cable. This higher duty has militated against the use of pipe-type cable in at least one instance.

In some cases, the fact that lead-sheathed cable of any type may be installed in existing or contemplated conduit systems (that may contain other cables) makes the use of pipe-type cable systems economically unattractive.

Extensive field and laboratory tests, running up to 70,000 hours and up to 140 degrees centigrade, on cable samples, supplemented by various test data obtained on paper tapes in oil, have indicated that oil-filled cable may safely operate at higher than the standard values used in this country for oil-filled pipe-type cables. This advantage amounts to about 10 degrees centigrade for normal operation and about 20 degrees centigrade for emergency operation. Perhaps, with more test data and some operating experience, higher operating temperatures than the standard values may also be established for some of the pipe-type cable systems. It would be of interest, for example, to learn what would happen to a 69-kv polyethylene-sheathed compression cable if its conductor were to operate for a week or two up to 115 degrees during an emergency.

Also, it would be helpful to learn the manufacturer's present ideas of the limiting temperature for say, 4,000 hours per year for 40 years, to which polyethylene may be subjected when immersed in oil, as described in the paper. At such temperatures, what is the expected condition of the polyethylene at the end of the assumed 40-year period?

This paper and other information have disclosed some marvelous 60-cycle breakdown test data as obtained in tests taking hours. Such data would indicate that substantial reductions in required insulation thicknesses are in order, especially for some pipe-type installations. However, as has been known for some time, the criterion to use in most cases is the unit impulse strength of the insulation, and this criterion has not disclosed any particular advantage for the pipe-type systems.

Also, Wyatt might expand on his statement about the English adopting a limiting maximum stress of 375 volt per mil for compression cable, because information that has come to my attention up to end of 1947 showed usage up to only 254 volt per mil.

In connection with the description of the taping for the normal joint of the polyethylene-compression cable by Palmer and DelMar, test data showing that the applied polyethylene tape is in effect "a seal" across the joint would have been of interest.

In general, it seems that the pipe-type cable systems will be the most suitable and economical for some conditions for operation at extra high voltages, but the nature and extent of these conditions are still being defined.

M. H. McGrath (General Cable Corporation, New York, N. Y.): The impulse data included in the DelMar section of the paper is interesting in that it emphasizes what would appear to be an inherent limitation of compression cable and one which may often be of importance on circuits connected to overhead lines. The average impulse breakdown strength of the tie line compression cable is given as 765 kv or only 14 per cent above the withstand level considered as standard on the 560 mil wall employed. On the other hand, the impulse strength reported today for the New Orleans cable is about 61 per cent above the standard level. DelMar suggests that this Titebilt construction may have been responsible for the relatively small margin on the tie line cable and cites a test on an experimental modification of Tirebilt on which the margin increased to 35 per cent. However, this is still much less than that obtained on the New Orleans cable. We can readily agree with DelMar's inference that adoption of the conventional taping practice will yield an improvement over Titebilt, but question whether this is the complete answer. An important factor in the problem appears to be the oval-shaped conductor which must be employed in the compression cable to obtain proper diaphragm action. By way of example, if one accepts the maximum stress obtained by DelMar on the tie line cable as the governing factor on impulse, we find that a round conductor with approximately the same perimeter as the oval conductor used would give the same impulse breakdown strength with a 460 mil wall as was obtained on the tie line cable with 560 mils. In other words, for the same surge strength a round conductor would have required 18

per cent less insulation thickness than was actually used. On normal frequency the maximum stress at rated voltage on the previously mentioned round conductor with 460 mils would be no greater than that accepted for the oval-shaped conductor with 560 mils. While it is true that the average stress on the round-conductor construction would be higher than on the tie-line cable, it would appear from the emphasis placed by DelMar throughout the paper on maximum stress that he considers the latter to be proper criterion at normal frequency.

A statement appears in the paper to the effect that the long lay skid wires reduced the coefficient of friction from 0.39 to 0.23. Presumably the 0.39 figure is intended to be representative for the usual skid wire construction in which the spacing between turns is about 1.5 inches. This value is materially higher than the 0.30 figure found in Table I of another paper[1] for straight pulls on the New Orleans installation where a short lay skid wire was used.

It seems possible that other factors may favor the use of long lay wires on compression cable. For example, might not a short lay wire tend to restrict the diaphragm action so essential to the satisfactory operation of compression cable? It would be interesting to know whether the tests reported on pressure differential and other characteristics of the diaphragm were made with the skid wires taut. Such a condition might occur in installation or service and interfere materially with the symmetry of the "breathing" action of the diaphragm.

It is also pointed out in the paper that one of the disadvantages of the lead covered compression cable design is the energy loss in the sheath. It might be mentioned that in the American design of unarmored lead covered compression cable, operation with open-circuited sheaths was contemplated because the heavy saturated webbing tapes used over the diaphragm reinforcement were designed to serve both as mechanical protection during installation and as low voltage insulation between sheaths and between sheath and pipe. It is easy to overlook the fact that energy losses comparable in magnitude to those with short-circuited lead sheaths can be introduced with skid wires, particularly if such wires are of high conductivity material and are applied with a long lay. For example, on the New York-Long Island compression cable, the energy loss in the skid wires, which we assume are copper, is estimated at from 50 to 75 per cent of the energy loss in a lead-covered compression cable operating with short-circuited sheaths. On the other hand, if a short lay construction had been employed, it is estimated that about two-thirds of energy losses in the skid wires of the tie-line cable could have been eliminated. The importance of this matter on the larger sizes of pipe type cable now being used in this country is obvious.

REFERENCE

1. 115-KV HIGH-PRESSURE OIL-FILLED PIPE CABLE INSTALLATION AT NEW ORLEANS, LOUISIANA, W. R. Bullard, A. D. Pettee, G. L. Rhodes. AIEE TRANSACTIONS, volume 67, 1948, pages 475–86.

A. D. Pettee (General Cable Corporation, New York, N. Y.): Throughout the first section of the paper one gains the impression that if a permanent polyethylene sheath is used on high-pressure pipe cable, fewer precautions are needed to avoid the harmful effects of moisture, and of contaminants which might be found in the pipe; also that less precautions need to be taken in installing the cable in rainy weather; further, that the sheath makes unnecessary a coating on the inside of the pipe.

The last section of the paper, however, indicates that exactly the same precautions were taken with this cable as have been taken with other pipe cables, which have not thus included in their construction a permanent sheath. The pipe in this case was carefully cleaned and coated internally, it was flushed with dried air to a minimum dew point, a plunger was blown through the pipe, the entire line was evacuated prior to filling, and provision was made for a tarpaulin cover to protect the cable if installed in wet weather.

In other words, none of the usual precautions for such installations were dispensed with, in spite of the existence of the permanent sheath. It is difficult to see the value of the extra factor of safety which is mentioned, since entirely adequate protection can be and has been provided in a less complicated manner, for example as is described in the paper discussing the New Orleans pipe-cable installation.

Secondly, in the first section of the paper, reference is made to the experimental gas-compression lead-sheathed cable in Detroit, in which one section of cable had holes punched in the sheath every 25 feet. I agree that this particular construction was stable during the period of the experimental operation, but it was of course not then a compression cable because there was substantially free access of gas to all parts of the cable. With a single hole, however, such as might occur and be undetected in a 30 or 40 year operating life, it seems to me then that two things are evident—first, the cable adjacent to a hole in the sheath or joint covering would immediately lose its compression characteristic. More important, however, portions of cable remote from the break would ultimately acquire an indeterminate pressure characteristic. High unbalanced internal pressure of 100 pounds per square inch or more during heavy load periods could lead to pronounced longitudinal migration of the impregnant in the direction of the break, at which point there would of course be no pressure differential. It appears that on the cooling cycle, with lower temperatures and viscosities, the expelled saturant would only be partially replaced by the pressure medium.

When an undetectable break in the cable sheath or joint covering could change the initial cable characteristics so much that the compression characteristic would be lost, and since the addition of the polyethylene sheath apparently does not eliminate any of the usual installation precautions, it appears to be not at all certain that the extra complications of the sheath are warranted.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): The paper by McCormack, Hatcher and their collaborators describes in a very interesting way the polyethylene-sheathed compression-type pipe cable that has recently been installed on Long Island. I will confine my comments to Wyatt's section of this paper.

Wyatt gives a very concise outline of the basic principles of the compression-type cable system. Briefly, this type of system consists of two separate compartments which for convenience will be termed the cable space and the pipe space. These compartments are completely and permanently separated from each other by an oil and gas-tight impervious cover on the cable and joints. All space inside this covering not occupied by copper and paper is completely impregnated with a noncompressible liquid oil or compound. The pipe space is filled with a fluid pressure medium, either oil or gas. The separator between these two spaces acts as a diaphragm, and pressure is transmitted to the cable space through this diaphragm and from the pressure medium. To perform correctly during temperature cycles the cable space must be completely filled with an incompressible liquid and this must have a sufficiently low viscosity to flow radially and fill up all shrinkage voids as fast as they are formed. The restriction to this flow is so high that shrinkage voids cannot be filled up with sufficient speed if any of the filling compound in the cable space is accidentally lost and is not immediately replaced by a similar incompressible liquid.

From the foregoing it is obvious that the principle of the compression system is based on permanent tightness of the diaphragm separator, and if this is lacking the compression principle is lost.

Assume that a restricted leak occurs in the separator. Some of the compound in the cable space will be forced out through this leak during periods of increasing temperature. If the pressure medium is oil, this will be immediately replaced during cooling periods and there will be no void formation or loss of pressure control. This replacing oil, however, must be completely free of impurities and have an insulating value equal to that of the compound it replaces. If the pressure medium is gas there will be no replacement of incompressible liquid, and gas-filled voids will form in near proximity to the leak. This gas under pressure will be rapidly absorbed by the cable insulation, and pressure control will soon be lost in these voids because gas cannot enter the restricted leak as fast as it is absorbed by the insulation. The net result is that after a period of time low-pressure voids can exist and there will be a possibility of ionization at these points, which can travel for some distance beyond the leak. If any impurities exist in the gas-pressure medium, they will enter the leak and tend to concentrate in the insulation. Plainly, with the compression system complete tightness and imperviousness of the diaphragm separator is necessary.

Experience has shown that this can be obtained with lead sheath and metal sleeves over the joints when great care is taken in the construction work. There is a serious question whether the same permanently impervious construction can be obtained with any form of organic covering. This is particularly true at the hand-wrapped seals between the cable covering and the joint covering. Experience with such hand-wrapped seals has never indicated that permanent tightness can be obtained under pressure cycles.

Considering all these things we believe that no attempt should be made to follow the compression principle with an organic covering. It is better to leave the joints open and allow free entrance of the pressure medium into the cable ends by use of pervious joint wrappings. When oil is used as a pressure medium, any displaced compound from the cable will be immediately replaced and no void formation will occur. It is obvious that the Long Island installation is safe in this respect because oil was used as a pressure medium in the pipe and every care was taken to completely remove impurities.

When gas is used as a pressure medium, however, the behavior is quite different from that of the compression system at a point of leakage as described previously. The gas can freely enter the cable ends and will be absorbed up to the saturation point by the insulation. Complete saturation throughout the cable run will be reached in a relatively short time and any voids that form in the meantime due to displacement of the compound will be accessible to full pressure adjacent to these feeder joints. After complete saturation is reached throughout, any voids that tend to form at any location will be maintained under full gas pressure. Obviously also there will be very little loss of surplus compound from the cable because of the covering. A number of long-time load cycle tests have proved that even without this covering there is not a sufficient loss of compound to affect ionization voltage stability, the compound being held in place in the high stress field by capillary and electrostatic attraction.

We accordingly believe that a true oil-filled or gas-filled pipe cable system has many advantages over a compression system when an organic covering is used instead of a metal covering.

Guy N. Everest (The Okonite Company, Passaic, N. J.): The authors' paper is very interesting and the following comments will perhaps bring out the need for more data which are lacking in the paper.

It is pointed out that the first step in simplification was taken in 1940 at Detroit. Perhaps it would be better to say that the first Oilostatic cable installation in 1932 was the initial step to prove a new technique in cable systems. Elimination of ducts and lead sheath was the first simplification.

It is pointed out that effects of rust, mud and moisture, and so forth, which may exist under field conditions as well as difficulties of pulling unsheathed cable in wet weather, led to a nonmetallic sheathed cable design. Anyone who knows field conditions will agree that none of these impurities are desirable regardless of the cable design, nor does anyone know if the polyethylene sheath will still be in existence five years or so from initial installation date. If it is not, then the cable will be just as much exposed as if it had no sheath on it. Secondly, if the sheath is not present five years or so from installation date, why go to the expense of installing it. Nonsheathed cables have proved themselves in pipe installations. Lead sheaths can be removed at a far smaller cost at time of installation than the polyethylene sheath can be supplied.

Means can be used to cover up cable pulling operations regardless of type of outer protective coverings.

The remark about the cable which was tested in Detroit and whose sheath was punched with holes seems to indicate no sheath is required and certainly the oilostatic installations have proved this.

The discussion disagrees with the theory that long skid wires reduce coefficient of friction. We obtain 0.24–0.27 on the short-lay skid wires, and we are of the opinion that the inner surface of the pipe is more of a controlling feature than the lay of the wires.

It seems that a lot of information is given about this compression cable at elevated temperatures, and with load cycles, however, there is a definite lack of data about this cable's behavior under cold conditions such as exist at the ends of the circuits. Are there any data available to show that ionization does not take place at 0 degrees Fahrenheit, that the polyethylene sheath is responsive at low temperatures, and that it will function correctly?

It will be interesting to follow the operating experience of the pressure tubes in the potheads because so far none have been in service.

The paper is further written proof of the feasibility and practical aspects of pipe-type cables. It would be interesting if some installation manhour figures were available, broken down into the various operations covering the installations as well as the overall cost of the installation. Engineers are interested in this information.

Wyatt in his reply to discussions said that 115 miles of compression cable have been in service for many years and none of the expected troubles put forth in the discussion have happened. However, Wyatt did not go further and explain that lead sheaths were used in all these. Now he claims some hope for his plastic sheathed cable. There is no service experience and it has to be obtained in the next few years.

Dr. Wilhelm Vogel (University of Cologne, Cologne-Mulheim, Germany): As one who, next to Hochstadter, was perhaps closest associated with the development of the compression cable, may I be permitted to add a few remarks to the paper. First, I want to express my satisfaction that Wyatt, Del Mar and their co-workers succeeded in overcoming the last deficiencies still present in the European installations of this type of cable, whereby it became competitive for all pressures and applications with the hitherto most generally used high voltage cable, the oil-filled cable, and may eventually surpass it.

To the history of the compression cable and of its first installations I might contribute the following remarks. After Hochstadter established in his original European researches the correctness of his fundamental idea that ionization phenomena in the insulation may be suppressed through the application of mechanical pressure over a flexible impermeable membrane, he turned to us[1] among others with the request that we cooperate in the technical development of the system. Thereupon we established the fact that an increase of the pressure over eight atmospheres (100 pounds per square inch) resulted in an increase of the steady-state breakdown strength to about 40,000 volts per millimeter (1,000 volts per mil). This put the economics of the new system on a firm basis because the reduction in the cost of insulation and conductor offset the additional cost of the jacket carrying the high pressure facilities. Thereafter I established that round cable cross-section is unsatisfactory for continuous operation and introduced the triangular form of the *SO* cable and later the oval-shaped single-phase conductors. Likewise, I introduced the steel pipe against the many conservative opinions of manufacturers and users, as the most advantageous carrier of the pressure medium. Cables in steel pipes were unknown conceptions for the European; and it also took part of the business out of the hands of the cable fabricators, yet they could not prevent the victory of the steel pipe. All our pressure-cable installations on the Continent are in pipes, while the English, scared by the difficulties encountered on the first installation with defective corrosion protection of the pipe, returned to armored lead as the pressure carrier. The first installation on the Continent was made at Muldenstein for the German Federal Railroad. Then followed installations in Scandinavia, whereafter German authorities, too, became more and more interested in the new system, not only because of its technical advantages and safety against aerial attacks, and sabotage, but also because of its minimum use of lead.

The task at the experimental installation at Arnheim was to develop a system capable of transmitting 100,000 kva at a normal voltage of 150,000 volts with the restriction, that in case of trouble reserve facilities must be available. While in all other systems a fourth single-phase cable was considered sufficient, we could offer for practically the same price two complete pressure-cable systems for each 100,000 kva. Here we had to employ the most stringent care with regard to the dimensioning of the conductor and the insulation, because the test conditions were extraordinarily hard; 10 per cent overload and 173 per cent over-voltage for more than 100 heating cycles. Here, too, we used a corrosion protective sheath containing quartz in order to minimize undesirable heat resistance.

As we succeeded by technical means to increase the life expectancy of the lead sheath considerably, it became clear to us that in this instance too, we had to break with traditions of cable technology the same way as we did eventfully in the case of the steel pipe. Many experiments were made among others, with sheaths of polyvinylchloride, which were not quite satisfactory. During the war, and even now, it is difficult to obtain more appropriate materials.

The use of polyethylene in the United States is a step in the right direction. The same is true in respect to abandoning the original system of cabling the three phases. The latter at the same time reduces the difficulties connected with the linear expansion of the cable in the pipe sufficiently, to forego the need for the heavy anchoring which we found necessary. Parallel to this is the simplification of the sleeves. On the other hand, I believe we had a priority in the development of terminals. With the co-operation of a German porcelain manufacturer[2] we were able to obtain pressure-tight porcelain insulators which stood up under severe tests, which consisted of heating cycles in water between +10 and +75 degrees

centigrade and under triple working pressure. These are still in actual operation.

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2. Steatit Magnesia A. G. Holenbrunn.

R. J. Wiseman (The Okonite Company, Passaic, N. J.): I wish to commend the authors of the paper for its thoroughness in presenting the theory and design of the system and the method used in installation. Some of the design features, such as the skid wire, safety head, and discharge tank on the semistop joint, and two manhole openings were developed by the Okonite-Callender Cable Company as a part of the development of the Oilostatic system.

Reference is made to the elimination of the use of a lead sheath on shipping of the cable and thereby an improvement in installation. The only improvement is the elimination of the manpower in removing the lead. When the Oilostatic cable system was developed, we were not certain what protection was necessary to take on shipping of the cable. To give maximum protection, we used a thin-lead sheath which can be easily removed. This has proved quite successful. We soon also realized that it would be desirable if a method were found to ship the cables without lead sheath. This has been accomplished so that today we are prepared to do so. However, do not get the impression because the cable is shipped nonleaded that it can be installed in all kinds of weather, rainy or dry. This applies to the compression-type, high-pressure gas, or Oilostatic cables. None of them are better off than the others in this respect, except those that use oil as the pressure medium. If moisture gets on the surface of the cable and is not removed during the evacuating of the pipe prior to setting up the pressure medium, the oil flowing into the pipe will wash off the surface moisture, if present, and as oil is flushed out, the moisture will be removed. This will not take place in gas filling. We have checked oil removed during the filling operation for dielectric strength and have obtained values over 30 kv, in many cases 42 kv.

There is nothing harmful in allowing the nonjacketed cable to come in contact with the atmosphere or in contact with the inside of the pipe. The insulation is covered with a shielding tape, and all who are acquainted with cable operation know that a shielding tape is a very good barrier to moisture penetration. Also, there is wrapped over the shielding tape a "*D*" wire or skid wire as specified in the paper. This skid wire raises the cable off the pipe but helps in getting a good grounding contact, provides a space to obtain flow of oil around the cable, and has helped in reducing the coefficient of friction.

Referring again to the *D* wire, we have used one and two *D* armor wires on installations, and we also get a coefficient of friction of 0.24–0.27, just as good as reported in this paper for three wires. The number of wires and their length of lay have no influence on the pulling friction.

Long length of sections is not solely applicable to the compression cable system. When we first introduced the Oilostatic system in 1932, we recommended at least 2,000-foot lengths, and if straight pulls, 2,500 feet. We soon found out that usually these lengths are not attainable, and a 1,500-foot length more common, with many even shorter. It is desirable to have as long lengths as possible in order to reduce the number of joints, but do not expect that only 2,500-foot lengths or higher will be used. There will be many shorter lengths, due to bends and obstructions.

Wyatt in his history of the development of the compression cable refers to the investigation made by Callender's Cable and Construction Company. It should also be stated that they studied the self-contained high pressure gas cable both in their own laboratory and also at Arnhem, Holland, with complete success in both cases for cable, joints, and terminals, no indication of increase in power factor or ionization after several years of testing.

Let us not get the impression from the paragraph on working stress of Wyatt's section that for the present this design, any more than any of the other designs of high-voltage cable, will be operated at a maximum stress of 375 volts per mil. Tests we have made on the Oilostatic design indicate very high working stresses, but we are held back from using them because of the limitations in the impulse strength of the insulation. When we know more about the effect of pressure on impulse strength, we can decide how far we can go in reducing insulation thickness.

The electrical tests reported by Del Mar and Merrell are comparable to what we have obtained on the Oilostatic cable system.

The installation of the system as written by Hatcher and DeTurk is quite complete. We note that the entire line was filled with oil at one time. This is possible for all designs using oil as the pressure medium. However, we feel that it is better to have semistop joints frequently located so that when the cables are jointed between them and the barrier plates located, it is possible to evacuate this section and fill with oil while other installation operations are taking place. This helps to reduce the time of installation and also because of the short lengths to evacuate, higher vacuum can be obtained. We would like to commend the authors for the completeness with which they have presented the subject.

F. W. Main (Enfield Cables Limited, Brimsdown, Middlesex, England): I am glad to have the privilege of contributing to the discussion on this compression cable group of papers, having done so much in the early days of development.

The thoroughness of the investigations in development, design and manufacture of compression cable with polyethylene-diaphragm sheath as detailed in the group of papers is to be admired and it has brought its reward in the amount of such cable being installed for commercial use.

The compression cable still leads supertension cables in high-safety factor and this has never been seriously challenged, but only the necessity for it!

It is interesting and satisfactory to learn that engineers in the United States have done so much to prove and then adopt the principles of the compression cable as detailed briefly in the first paper, aided by reproduced curves referring to the early British investigations and developments.

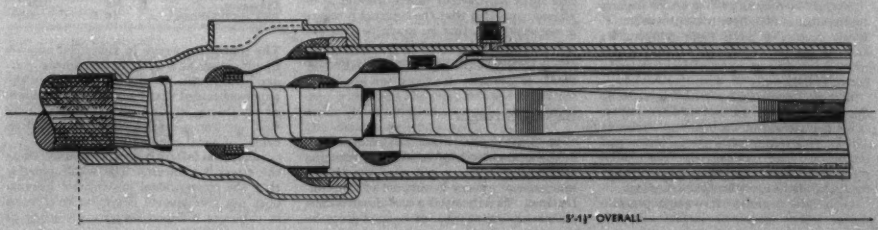
The authors have made adjustments to European designs to meet prevailing conditions in the United States.

In some types of supertension cables employing gas under pressure, so-called harmless gaseous ionization is acknowledged and tolerated, but in the compression cable I must confirm the authors' statement that ionization is entirely suppressed.

In the United States you have cables other than the compression cable in pipes but, as correctly pointed out by Wyatt, no costly cleaning of inside surface of pipe or special treatment is necessary for housing the compression cable as the pressure medium does not contact the cable insulation—it is not part of the active dielectric.

We cannot agree with the statement that the European compression cable design is clumsy, it may be liberal in reinforcement precautions and materials but 10 years of successful service of the pipe-line cable in Great Britain and eight years of service of self-contained cable have justified any apparent conservatism in early designs. As far as complicated joints are concerned we would refer to Figure 1 of the discussion, illustrating the 132-kv joint installed in England. Comments are unnecessary. We consider the American joint as shown to be more complicated than ours.

Figure 1. 132-kv joint installed in England



In Britain an alternative for lead as a pressure conveyor diaphragm to the dielectric has been sought, but the plastic field has not been acceptable due to the inherent porosity and in the case of polyethylene, swelling of the material when in contact with oil at elevated temperatures, which effect we have confirmed. We would like to know whether the polyethylene available in Great Britain is similar to that obtained in the United States.

It will be interesting to watch these installations in the United States to see whether, with the precautions taken, the anticipated length of life of the polyethylene inner sheath is justified; we consider there is a greater chance when using oil as the pressure medium than when employing nitrogen gas. We do not favour oil as the pressure medium as it involves plant to deal with changes of volume and movement as load varies in the cable and the effects of changes of gradients, whereas, the use of gas calls for no accessories.

We have not found it necessary to use cathodic protection as the Enfield Sandwich is sufficient.

As we use stabilised insulators our porcelains are single shedded.

We note that you are successful in the States in drawing three heavy cables into one pipe.

A. L. McKean (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): The 3-phase laboratory replica of the 138-kv tie line has been opened for inspection after load-cycling under voltage for one year. A total of 127 load cycles, each of 48-hours duration, have been completed, 60 cycles to 70 degrees centigrade, 67 cycles to 85 degrees centigrade conductor, average temperature measured by change of copper resistance. Due to uneven circulation of air and the cooling effects of the copper risers, hot-spot cable temperatures were from 5 to 10 degrees higher than average.

The aged cable, including terminals, proved to be in excellent condition, appearing like new. Visual inspection revealed no signs of deterioration. This was further substantiated by comparison of radial power factor tests at 100 degrees centigrade, results after test being identical with those on new cable.

Careful visual examination of the polyethylene sheath after test revealed no adverse effects from the load-cycling. Tensile test results, equivalent to those of newly extruded sheath, confirmed the visual inspection.

The electrical behavior of the cable during all phases of the load-cycle test was essentially stable.

Normal load-cycle testing to 70 degrees centigrade did not affect cable power factor. Power factor, at both room temperature and 70 degrees centigrade conductor, remained constant at 0.31 per cent, ionization factor at 0.01.

Likewise, controlled overload tests to 85 degrees centigrade did not affect stability. After completion of 67 overload cycles at 100 kv to ground, cable power factor and ionization were unchanged. The radial power factor tests at 100 degrees centigrade, mentioned before, confirm these findings.

Load-cycle testing at zero gauge pressure during 5-load cycles, at various intervals throughout the test period, demonstrated the system's stability under "emergency" conditions. The longest continuous-zero-pressure operating period was for 96 hours, 2-load cycles.

It was also shown that sudden removal of pressure and full load, simultaneously, did not immediately result in ionization; instead ionization increased slowly to a maximum upon complete cooling of the cable. Apparently, the initial rapid cooling of the conductor does not induce immediate ionization because of the reversible elasticity of the oval cable structure. Moreover, restoration of load, without pressure, reduces ionization, gradually, to the original negligible value.

Reapplication of pressure immediately eliminates ionization and restores cable power factor to its normal low value.

Ovality Ratio and Expansion

W. A. Del Mar and **E. J. Merrell** (Phelps Dodge Copper Products Company, Yonkers, N. Y.): Atkinson's attention is called to the fact that the test results plotted in Figure 6 of the paper are for cable without external pressure. When a pressure of 200 pounds per square inch is applied, the long sides of the oval flatten and the ovality ratio drops to about 0.85, as indicated in Figure 5 of the paper. This allows a greater change of ovality than that calculated by Atkinson but is not sufficient to accomodate all the expansion. No compression cables, however, are designed with that in view. The designs are made by balancing the expansion of copper, paper, oil and sheath against the increase in volume resulting from compression of the paper, change of ovality ratio and the elastic stretch of the sheath to the extent allowed by the slight give of the bronze tapes. No allowance is made for poor impregnation, as the impregnation is necessarily very thorough in order to meet the stringent ionization test.

Flow of Polyethylene

Atkinson asks if the cyclical changes of pressure will cause a flow of polyethylene from the long axis to the short axis, due to the greater pressure at the former points. The answer is a definite "no". In none of our many load cycle tests has any such change occurred. In fact, the sheath is always a trifle thicker on the long axis than on the short axis.

Joints

Halperin asks about the polyethylene seal on the joints. Load cycle tests have shown this to be satisfactory under working conditions, provided that the internal pressure is not excessive, that is, much beyond the amounts to be expected in service. Hatcher's discussion gives further data on this point.

Impulse Strength

McGrath believes that the small radius of the oval cable has the effect of reducing the impulse strength and cites the "maximum stress" theory in support of this contention. This theory is a useful one if used within its limitations, but it should not be used indiscriminately. The idea that impulse strength is a function of maximum stress, derives principally from a paper by L. I. Komives.[1] Tables I and II of that paper cite 38 impulse tests on ionization-free cables of American manufacture and give the calculated maximum stresses, which I have averaged for each conductor size, as follows:

Conductor Radius Mils	Calculated Maximum Stress Volts Per Mil (Average Values for Each Conductor Size)
945	2,840
542	3,350
515	3,296
514	2,540*
514	3,290
290	3,118

* Cited as "very irregular".

It will be seen at once that the maximum stress theory seems to act in reverse, so to speak, the cables with the smaller conductors failing at by far the greater "maximum stress". This is not a new idea; it has been known for years that if two cables are designed for equal "maximum stress", one with a small conductor and thick insulation and the other with a large conductor and thin insulation, the one with the small conductor will have far higher breakdown voltage.[2,3,4] In other words, the maximum stress theory, when applied to breakdown voltages does not agree with fact, if conductor radius is taken as the variable. I therefore cannot give much weight to McGrath's criticism of the oval-shaped conductor. Perhaps the reason why the maximum stress formula is used by McGrath is that in cables of non-circular cross-section, failure usually occurs at a region of small radius. This fact is recognized but it does not prove the correctness of the logarithmic formula for variable conductor sizes.

The maximum stress appears to be a useful guide, however, if insulation thickness is the variable. This may be seen from Komives'[1] Table II where data are given for two cables of equal conductor radius dut unequal insulation thickness. These data are as follows:

Conductor Radius Mils	Insulation Thickness Mils	Average Stress (Average of All of Each Type)	Maximum Stress (Average of All of Each Type)
945	315	1,634	2,440
945	500	1,386	2,240
Difference		248	200
Per Cent Difference		15.2	8.3

The maximum stress, in this case, appears to advantage as a guide to breakdown voltage. McGrath, looking at Figure 7 in the paper concluded that I accept the maximum stress theory in toto. He failed to note that the tests recorded therein were all on cables of equal conductor size.

Insulation failure begins, of course, where the stress is greatest. The whole point is that the conventional logarithmic formula does not give correct comparative stresses in cables of different designs. A study of

W. D. A. Peaslee's discussion,[5] and P. L. Hoover's paper[6] may afford the clue to these discrepances.

While the use of high-density high-elasticity paper for the outside layers of insulation gives lower impulse strength than when such paper is used for the inside layers, the ability of the cable to operate without pressure is superior with the former construction, due to the elastic tightness with which the interior layers of insulation are held. It is a matter of judgment, based on local conditions, which of these factors should predominate.

The conclusion which I draw is that there is nothing about the compression cable design which puts it at a disadvantage in the matter of impulse strength.

Loss in Skid-Wires

We have made a 3-phase set-up in our laboratory which is a short replica of the Long Island tie line and have made some tests to check McGrath's figures on losses in skid-wires. Of course we could not insert an ordinary ammeter in the skid-wire circuit because its high impedance would cut down the current in a circuit of such low resistance. Instead we have used a Ferranti clamp-on ammeter and checked its effect by means of a high-amplification oscilloscope calibrated as a voltmeter. This revealed that when the ammeter was clamped on, there was a negligible change of current.

McGrath takes the sheath loss in an equivalent group of lead sheathed cables with sheaths short-circuited, as his basis of comparison. We estimate this as 315 watts per 1,000 feet of each conductor.

We made our tests first with the skid-wires in natural contact with one another and with the pipe as they actually are in the tie line and then with the skid-wires insulated from the bronze-binder tapes and from one another. In the latter case the current varied between 9 and 10 amperes per skid-wire with 400 amperes in the main conductors. McGrath's estimate that the loss would be between 50 and 75 per cent of that in the short-circuited lead sheaths, means a loss of 157 to 236 watts per 1,000 feet per phase, which is equivalent to a current of 7 to 9 amperes per skid wire. This is in fair agreement with our measured 9 to 10 amperes.

However, when the skid wires were uninsulated and in their natural electrical contacts, the rms value of the current in each of them was only 3.7 amperes. With this current the loss would be 41 watts per 1,000 feet of cable instead of McGrath's 157 to 236 watts.

Expressed as a percentage of the line loss, this is less than 0.7 per cent and expressed as a percentage of the loss in short-circuited lead sheaths, 13 per cent.

While the loss may be somewhat less with short-lay skid-wire, it is quite negligible on cable of this size, and subordinate to the advantages of a long lay. For large sizes, skid wires of metal of higher resistivity may be found desirable in order to retain the advantages of long lay.

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Kenneth S. Wyatt (Phelps-Dodge Copper Products Corporation, Yonkers, N. Y.): Our description of this simple and relatively fool-proof pipe cable system has come in for considerable criticism at the hands of Pettee, Atkinson, McGrath, Wiseman and Everest. Del Mar, in his discussion, has answered several important criticisms by actual experimental work. In answering the remainder, I wish to focus sharply on a number of hard facts. In the first place, installations of the compression cable with lead sheath abroad, from 66 to 120 kv, now total about 100 miles. They are all giving good service, the first one in London for sixteen years. There are now fourteen miles of the simplified system with plastic sheath operating satisfactorily in this country, and there will be considerably more before the end of 1948. This means that in both operating mileage and mile-years the compression cable is second only to the Pirelli oil-filled system. None of our critics can point to a record like that. However, I fully agree that all possibilities of trouble should be considered even though mature deliberation shows them to be purely imaginary.

When the cable was equipped with a continuous sheath and was in effect a factory-sealed unit, why did we clean and coat the inside of the pipe and take precautions against the entry of moist air on the Long Island installation? Simply because it was a first installation in this country and the engineers concerned considered this a prudent move. None of the pipe abroad was freed from rust nor were any elaborate precautions of any kind taken because the pipe was considered to be merely a duct. We do not clean the inside of a concrete duct bank for oil-filled cables. As a matter of fact, on our installation of 138-kv compression cable at Milwaukee, it was agreed to make one pull a day regardless of weather. The cable was pulled with scanty tarpaulin cover in one case in heavy rain, in others at temperatures as low as −15 degrees centigrade, with 12 inches of snow on the ground, and, in general, under difficult field conditions. We, of course, require that the pipe line be evacuated until any moisture taken in with the cable is removed.

Thus, it could hardly be said that field operations with this cable are at all critical. Contrast the experience at Philadelphia, in 1947, where between May first and August first they had 78 days of rain, during which there was not any cable stripped of sheath and pulled into the pipe, and as a result the labor stand-by time increased the cost of the installation significantly.

In the oral discussion, our continuously sheathed cable was called an anachronism and an atavism. They said we should strip off the sheath. I well recall a speaker saying last year, in an Edison Electric Institute meeting devoted to finding ways of reducing underground cable costs, that since some manufacturers stripped the sheath from the cable before pulling into the pipe and filling with oil, why not go one step farther; instead of putting on a sheath only to be stripped off again, why not just insulate the cable with dry paper at the factory, pull into the pipe, and dry, evacuate and oil-impregnate in the field? Other discussors soon brought out the point that the field is the last place to do such a delicate job, as conditions of humidity, rain and mud are beyond control. The costs would be increased enormously and that for a high quality and economical job every operation possible should be done in the factory where conditions can be controlled. We agree with this view, and we strongly believe in sealing the cable at the factory with a continuous sheath so that we are not dependent on the vagaries of field conditions.

Pettee states that his sheath of polyethylene and metal tapes, which he says keeps the air and moisture out during shipment, but freely lets the nitrogen or oil in when it is installed in the pipe (by some subtle mechanism beyond my comprehension), is more economical. With such taped-sheath cables very special reels are employed which represent an appreciable additional expense. The experience to date with such a taped sheath is that during shipment most of the cable lengths leak several gallons of oil. The present standard reply to that is, "What does a leakage of a few gallons of oil matter in a 2,000 foot length of cable?" I think it matters a great deal in a cable at 100 kv or over, because a few gallons of air must have leaked in where the oil leaked out. This means potential ionization, and that means, in turn, a weakening of the cable. It is now over twenty years since I started taking apart extra high-voltage cables that had deteriorated and failed at Detroit, and the whole of this experience is against such indeterminate improvisations as the taped sheath.

Halperin and others request more information on the permanency of the polyethylene sheath. Quite apart from the very considerable engineering advantages of a polyethylene sheath, that is, its light weight making for long-pulling lengths and elimination of two out of three manholes and its nonconducting properties eliminating sheath losses, polyethylene has other important advantages over lead as a sheathing material. Although it has a technical softening point at about 110 degrees centigrade, it is extruded on the cable at 185 to 190 degrees centigrade, a temperature not far below the extrusion temperature of lead. Polyethylene has an elongation of over 400 per cent in sheet form, somewhat less in sheath form, whereas lead will split or crack with only a small fraction of this elongation. There is a complete absence of oxide seams such as have been found with lead. Polyethylene does not increase the power factor of the insulation layers in contact with it as does lead. It is a homogeneous material without plasticizers or fillers; it is crystalline and will not cold flow at loadings below the cold-drawn point at temperatures up to approximately 90 degrees centigrade.

Polyethylene is a tough leathery material which should withstand millions of cycles of flexing without rupture. For example, the polyethylene insulation of coaxial cable up to $^7/_8$-inch diameter, when subjected to

twist tests consisting of 360 degree twists in 17 feet, withstood over 400,000 cycles without failure, termination of the test always being due to breaking of the copper. In another test strips of polyethylene sheath taken from the Long Island cable sheath were flexed in the University of Illinois lead-sheath tester. Whereas copper-bearing lead usually fails in about 7,000 cycles, polyethylene so far appears unchanged after 298,000 cycles. For these reasons the polyethylene sheath, reinforced with bronze tapes so as to have a bursting strength of over 500 pounds per square inch, is, in our experience, far more durable than a lead sheath.

Although polyethylene has the highest impermeability of any commercial organic plastic material, it will pass moisture vapor at an extremely low rate over a long period of time if a moisture-hungry material is present on the other side. Polyethylene itself does not absorb water (less than 0.02 per cent by weight). However, we believe a polyethylene sheathed paper cable if submerged in water would operate satisfactorily for 10 or 15 years. In the steel pipe, moisture vapor is evacuated before filling with the 200 pound pressure medium.

All that we know physically and chemically about polyethylene leads to the conclusion that it should be absolutely permanent. It consists of alifatic carbon and hydrogen chains, very similar to paraffin wax, but with very long chains with some cross-linkage to give it toughness. Paraffin wax, as is well known, is chemically inert and does not oxidize or change with time. Polyethylene communication cables laid under the sea have now given nearly 10 years of satisfactory service. In consequence it has almost entirely replaced guttapercha and rubber for this purpose, and installations of undersea polyethylene cable represent very substantial investments. More recently polyethylene is being used to replace lead as a sheath for telephone cable on a mass production basis. In this case a thin metal covering, perhaps 20 mils in thickness, over the polyethylene serves to prevent the diffusion of moisture vapor through the polyethylene. This activity has necessitated the building of new polyethylene plants in addition to new cable production facilities. It represents an investment of many millions of dollars, which has been based on investigations over many years by an extensive and capable research organization, of world-wide reputation. Polyethylene is thus regarded by the best technical opinion in several countries as an absolutely permanent sheathing material.

In the case of power cables there is the complication of impregnating oil in contact with the sheath. Polyethylene swells in oil at temperatures above 65 degrees centigrade, although the effect is reduced the higher the viscosity of the oil, and the higher the viscosity index. A metal-reinforced polyethylene sheath does not swell in oil at 70 degrees centigrade, nor does the tensile strength diminish. Just as rubber cannot absorb water when restrained to prevent swelling, so it appears that polyethylene when restrained cannot absorb oil. Used on experimental cables it has been completely satisfactory. Two lengths of 138-kv cable identical with that used on the Long Island line were each installed in a vertical pipe 80 feet high, one at atmospheric pressure external to the sheath, and the other at 200 pounds per square inch nitrogen. The ends were sealed. The cables have been given one load cycle a day to 45 degrees centigrade above ambient. The two cables are behaving perfectly normally after eleven months, there being neither leakage of oil through the sheath nor permanent change of ovality. Likewise, in all our work load-cycling compression cable with reinforced polyethylene sheath in pipes filled with oil or with gas we have never observed any damage to the sheath in the form of swelling, tears, or pinholes, even though these tests have run 200 and 300 load cycles to 85 and 90 degrees centigrade copper. Finally, we must point out that the present allowable copper temperature for 138 kv cable is 70 degrees centigrade, which corresponds to a sheath temperature of 51 degrees centigrade. At this temperature polyethylene does not swell in oil even if not restrained with bronze tapes.

We believe that if an oil-compression cable is operated continuously at 70 degrees centigrade sheath, which corresponds to a copper temperature of 100 degrees centigrade, the sheath is safe. The gas-compression cable we believe is safe to 90 degrees centigrade sheath, corresponding to a copper temperature of 125 degrees. We hope this will satisfy Halperin. However, we are not satisfied that oil-paper insulation on 138-kv cables is safe at 100 degrees centigrade or over, since at this temperature we are approaching very closely the thermal instability point of the paper insulation. We think it sounder practice to use a larger conductor size.

A warning should be given regarding statements as to the properties of polyethylene. There are many grades, according to molecular weight. A medium molecular weight grade is a soft paste-like material. Then there are several types of polyethylene; the product of some manufacturers swells more in oil than that of others; those that swell least have a greater degree of cross-linkage than others. We are careful to employ a high molecular weight polyethylene which exhibits a high degree of cross-linkage.

Experience is important in extruding polyethylene sheath. It is of the highest importance to eliminate strains. This means close temperature control. We have extruded about sixty miles of polyethylene sheath on single-core cable, yet we have never seen a pinhole; and all has been substantially free from strain. Accordingly, if a statement were made to us that a polyethylene sheath has not passed certain tests, we should interpret it to mean that the wrong type or grade of polyethylene was used, or the extrusion equipment was inadequate, or that the manufacturer of the sheath lacked the necessary experience.

Utility engineers associated with the installation of both gas- and oil-compression cables have expressed the opinion that if all the dire predictions came true and the plastic sheath disappeared completely 15 minutes, or 15 years after installation, the cable would be at least as good as sheathless or taped-sheath cables in pipes filled with gas or oil under high pressure from that time on; and prior to the disappearance of the sheath the cables would be much better because installation would have been simplified and the initial high factor of safety would have been maintained unimpaired by deterioration up to that point. On our part we can only restate our conviction, based on seven years experience with polyethylene and three years of experimental work with polyethylene-sheathed compression cable, that the sheath will have as long a life as our steel-pipe system. As to that, we doubt whether any electrical engineers who have now graduated will live long enough to find out the answer.

Atkinson raises the question as to the adequacy of the oval form in compensating for oil expansion. Experimentally, it has been conclusively demonstrated many times during the last twenty-two years, both here and abroad, that the oval does not become circular even under maximum permissible overload. The stroke on the minor axis has been carefully measured by capacitor and other methods. Has Atkinson forgotten how carefully we went over this ground in 1939–1940 at the time he became licensed to make this cable? And has he forgotten his own experiments dated March 22, 1940, just prior to his making the Detroit experimental compression cable, which showed that the most thoroughly impregnated cable he could make exhibited a compressibility at 200 pounds per square inch external pressure of approximately 1 per cent of the insulation volume? However, we are pleased he has raised this point, as it will have interest for many cable engineers.

All oil-paper insulation exhibits some compressibility due, it is believed, to minute cells in the paper which are not filled with oil. Del Mar has given a fuller discussion of the ovality compensating action.

Discussions by Shanklin always command our respect because for twenty years he has been a chief advocate of maintaining oil-paper insulation completely filled with oil to obtain high dielectric strength and high factor of safety in extra high voltage cable. In his 1942 discussion of the Faucett, Komives, Collins and Atkinson paper,[1] Shanklin states,

"Experience . . . . . indicates that it is not possible to avoid occasional leaks in the (lead) sheath envelope".[1]

Six years later he now says,

"Experience has shown that complete tightness and imperviousness of the separator can be obtained with lead sheath and metal sleeves over the joints when great care is taken in the construction work."

Obviously the continuing satisfactory service of great mileages of gas-compression cable (lead sheath) abroad has caused Shanklin to modify his earlier opinion. But now his worry is transferred from the lead sheath to the polyethylene sheath, which is understandable. He states the oil-compression cable is safe. The gas-compression cable worries him because if a restricted leak in the sheath occurs,

"some of the compound in the cable space will be forced out through this leak."

Later he answers this doubt himself by pointing out,

"Obviously there will be little loss of surplus compound from the cable because of the covering. A number of load cycle tests have proved that even without this covering, there is not a sufficient loss of compound to affect ionization voltage stability, the compound being held in place in the high stress field by capillary and electrostatic attraction."

Experimentally we know that a metal-

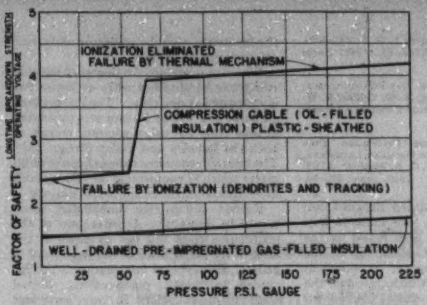

Figure 2. Effect of pressure on safety factor

reinforced lead sheath with three oil-paper tapes under the reinforcing tapes does not leak oil under load cycle conditions when a six-inch split in the lead sheath occurs at an oxide seam; nor is the cable harmed electrically. We attribute the absence of leakage more to the metal reinforcing and paper bedding tapes, and to the relatively balanced pressure across the sheath than to capillary and electrostatic attraction about which we are frankly skeptical. In England a 132-kv gas-compression pipe cable was load-cycled to 90 degrees centigrade copper for a year at a maximum stress in excess of 280 volts per mil. A load cycle without pressure indicated a small amount of ionization on the cooling cycle. Upon reapplication of pressure this ionization disappeared. Later, upon dismantling the installation, the longitudinal split in the lead sheath referred to before was discovered. Evidently the small amount of ionization without pressure was due to the entry of gas. This and other experimental evidence have led us to the conclusion that if the sheath of our compression cable is made reasonably tight, its high reliability characteristics will be maintained. The impressive service record supports that conclusion.

Shanklin is mistaken in supposing that the gas in the pipe can freely enter the cable ends. We have gone to some pains to prevent this happening. Gas pressure is communicated to the oil in the terminals by a compensator metal membrane. A gas-tight gland separates oil in the terminal from gas in the pipe. The compensator maintains a balanced 200 pounds per square inch pressure across this gland, and the differential across it at any time is not more than a few pounds.

The oil does not flow radially through the paper layers of an oval cable to any extent, as Shanklin points out. Rather, oil and paper distend outward as a unit on the minor axis. On the load cycle there is a tendency toward compression of the paper on the major axis and reduction of the oil film thickness, and vice versa on the minor axis. Accordingly, there is a slight movement of oil circumferentially in the interlayer spaces. On the cooling cycle this interlayer flow occurs in the opposite direction.

Everest requests data as to the cable's behavior at 0 degrees Fahrenheit at circuit ends. Polyethylene is flexible down to −70 or −80 degrees centigrade. As to the functioning of the compression cable principle at 0 degrees Fahrenheit, we refer him to the installations at Oslo, Norway, which has been operating without incident for fourteen years, and at Stettin, Germany, which has been operating satisfactorily for thirteen years.

Everest piles supposition upon supposition, assuming that, in time, the polyethylene sheath will disappear. We have presented evidence that it will not disappear. I am much more concerned about the rubber gaskets in his installations, where the steel joint sleeves are fastened to the pipe all along the line. It is well known that rubber contains accelerators, often sulfur and chlor-compounds, which cause it to age. However, we have not questioned the soundness or permanence of his system. If criticism is made sufficiently destructive, few modern engineering structures would escape some question as to permanency. For example, the rate of creep of steel under high pressures such as obtain at the base of our skyscrapers has raised criticism in some quarters. The only answer to such criticism is the same as we have given in connection with compression cable—look at the operating record.

Several discussors refer to the coefficient of friction as a function of the number and length of lay of the skid wires. Faucett, Komives, Collins and Atkinson,[1] in pulling 120-kv cable with oiled fabric cover at Detroit obtained a coefficient of 0.54. By using one D-shaped skid wire applied with a $1^1/_2$-inch lay on a cable of similar size, they later reduced this to 0.37.[3] At New Orleans, Bullard, Pettee and Rhodes,[4] increased the number of skid wires to two, and the lay to three inches, and thereby reduced the coefficient to 0.30. In the Long Island compression cable we simply used three wires and a lay of 12 inches and still further reduced the coefficient to the value given by Hatcher, namely, 0.24. However, other factors may modify these results, for example, if the pipe is a tight fit on the cable, in addition to the normal points of contact on the bottom of the pipe, the skid wires will be forced against the pipe on the top and sides; if the conductor is large, it will act as a strong spring, forcing the skid wire against the pipe at these points. Wiseman has had experience with such cases, and this may explain the contrary results.

In reply to McGrath, it is essential when using a 3-wire long-lay skid construction to apply the *D*-shaped wires tightly. Obviously, even if the skid wire were applied to our oval cable circumferentially without any lay, it would flex toward circular just as do the bronze reinforcing tapes. In going to a 12-inch lay, we had no thought in mind other than to reduce the coefficient of friction.

In the oral discussion we gladly gave credit to Wiseman for the skid wire idea, and only claimed an improvement in construction with three wires instead of one, and a lay of 12 inches, instead of $1^1/_2$ inches. Del Mar has since called my attention to an actual piece of the original 1931 pipe cable in London mounted on his laboratory wall, and this has a skid consisting of a rectangular steel strip $^1/_4$-inch wide applied with $3^1/_2$-inch lay. Hence we can now only give Wiseman credit for rounding off the edges.

These and other items appear to us to be details which should not distract us from the central facts, namely, Martin Hochstadter introduced the pipe-type high-pressure extra high voltage cable in London in 1931. He used a welded-steel pipe line protected against corrosion laid under the streets. Into this he pulled a sheathed cable. Then he filled the pipe at the ends with 200 pounds per square inch oil and in the main part of the line with 200 pounds per square inch nitrogen. Later Hugo Sonnenfeld proposed the same system, but with the sheath stripped off, and C. E. Bennett actually made such a system, limiting himself to oil as the pressure medium. The Sonnenfeld–Bennett modification had some justification in that the great weight of the lead made for short pulling lengths, more manholes, and in addition the lead resulted in serious sheath losses.

As Vogel aptly points out, to make the pipe cable a real success, it was not sufficient to break with tradition in the matter of the pipe alone; it was essential to break with the tradition of lead as a sheathing material, and to replace it with a light-weight tough and inert material, such as polyethylene.

Today with the advent of the plastic sheath, there is no engineering or economic reason for stripping the sheath from a cable—only commercial reasons.

But Wiseman has made a great contribution to pipe cable development in this country. He has spent over ten years in formalizing the current carrying capacity calculations of such cables, and in determining pipe cable constants. He has also investigated transient conditions in pipe cables, using the thermal transient calculator at Columbia University. Wiseman and Bennett worked out in a practical manner the oil-pressure and expansion system for oil-pipe cables. Their advocacy of the pipe system over long years did much to make it acceptable for American practice. The final proof of the practicability of buried pipe for high-pressure cable systems was the 7-mile 120-kv line in Detroit in 1941, which withstood successfully 200 pounds per square inch nitrogen pressure for six years without any leakage in pipe, and which was absolutely free from corrosion due to the use of a heavy well-tested pipe coating.

I am surprised at Everest questioning the terminal design, since he is using terminals of identical design and manufacture on his own 138-kv Oilostatic cable at Maspeth, Long Island, which is now in operation. The same design was used on the 138-kv San Antonio pipe cable which has been operating for several months. The same design is also being used for many other

pipe cable installations and the National Advisory Council for Aeronautics high pressure self-contained cable.

Halperin fears a 3-phase short. If this is going to occur anywhere, it will be at the joint. We can guard against this by use of a grounded-metal sleeve over each core. Today with fast-acting relays there would seem to be no excuse for a 3-phase short in a pipe system.

In reply to Vogel, the British are not "scared" of steel pipe. One reason for their cautiousness is the heavy penalties their courts may exact if cathodic protection on the pipe causes damage to nearby structures. Their crooked narrow streets require many bends of short radius. There is now a swing in thinking in Great Britain toward steel pipe construction, and it is probable more such lines will be put in in the near future. I believe the steel pipe duct for high pressure cables was first proposed by F. W. Main.

Halperin thinks the number of designs for extra high voltage cables is now excessive and some should be eliminated. It will require several years to crystallize opinion in favor of perhaps three or four designs. Certainly every cable engineer would prefer gas to oil as the pressure medium between cable and pipe because of absence of pressure differentials along the line resulting from sudden changes in load or due to profile, and because of absence of complicated joint accessories, pumping station and expansion reservoirs. On the other hand, he would prefer to have the paper insulation completely filled with oil so as to realize a high factor of safety against long-time breakdown. This combination of gas in the pipe for simplicity and 100 per cent oil-filled insulation for high safety factor can only be obtained by employing a continuous sheath over the insulation. If such a sheath is used, a means must be found for compensating for differential expansion of the oil. This may be done in a simple manner by making the cable oval in cross-section. When these three steps have been taken, you have a compression cable. The small extra cost of the sheath is justified many times over by the high-safety factor thus obtained, Figure 2 of the discussion, the permanency throughout life of this safety factor, and the savings in installation costs due to a moisture-proof sheath. One other important advantage accrues from this construction, that is, the ability to operate for 24 hours if pressure is lost. In special cases the pipe may be filled with oil. These advantages are so outstanding that we believe the compression cable system will have a permanent place among our standard cable types.

The system described, and other pipe systems, bear the creative stamp not only of the manufacturers, but also of the utility engineers. For example, the use of polyethylene sheath, the long-lay skid wires, and many other novel elements of design. It has required courage and foresight on the part of the utilities to embark on these new designs, and great credit is due them for the present progressive state of the art.

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3. 120-Kv HIGH-PRESSURE GAS-FILLED CABLE, I. T. Faucett, L. I. Komives, H. W. Collins, R. W. Atkinson. AIEE TRANSACTIONS, volume 61, 1942, pages 658–665.

4. 115-Kv HIGH-PRESSURE OIL-FILLED PIPE CABLE INSTALLATION AT NEW ORLEANS, LOUISIANA, W. R. Bullard, A. D. Pettee, G. L. Rhodes. AIEE TRANSACTIONS, volume 67, 1948, pages 475–86.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): Certain questions have been raised by the discussers of the paper pertaining to the justification for selecting the compression-type cable, and tests which justified the selection of this type of cable. The following discussion covers these questions.

Halperin questions the ability of existing circuit breakers to interrupt a possible phase-to-phase failure in the pipe. This type of failure has been considered and the circuit breakers on our system are of sufficient capacity to clear such a fault. In addition the relay settings are such that it is expected a fault will be cleared before it develops into a phase-to-phase fault. Halperin also questions the economy of a pipe-type system as compared to an oil-filled cable system on the basis that existing conduit systems could be used for an oil-filled cable system. Installation of 138,000-volt feeders on the Consolidated Edison system has always required the laying of new conduit lines since existing conduit systems with cables installed therein limit the carrying capacity of the 138,000 volt feeder, and in practically all cases the manholes would have to be enlarged so as to provide space for racking of the cables and installation of reservoirs. Estimates for the installation of a pipe system as compared to a conduit system for oil-filled cable have shown a saving in favor of the pipe system, and actual costs of the present installation are bearing out this fact.

In reference to Halperin's question concerning the effectiveness of the polyethylene tape seal across the joint, it is well to point out that laboratory tests were made on a joint sealed in this manner. A test joint was made and taped with polyethylene tapes. This joint was immersed in water and subjected to a gas pressure of 24 pounds from each end of the test sample for 67 hours. No leaks were found during this period. The pressure was then raised to 50 pounds and held for 100 hours. After 70 hours some signs of leaking were noted. Additional signs of leakage were noted at the end of 100 hours. The points of leakage were determined and the design improved to prevent leakage so that with the joint as installed in the line it appears that a satisfactory seal has been obtained.

Pettee questions why precautions were taken to clean and protect the inside of the pipe in the same manner as has been done for other pipe-type installations if the polyethylene jacket is considered to be a satisfactory seal. In regard to this question, we felt that it was desirable to treat the pipe in the same manner for two reasons. First, it was the first installation of this type which had ever been made, and second, it appeared to be desirable to maintain the oil in satisfactory condition so that if it ever entered the cable it would not affect the insulation, thereby adding an additional safety factor to the line.

Wiseman questions the degree of vacuum which can be obtained when an entire line is evacuated at one time. The line between Jamaica and Valley Stream was evacuated at one time by using vacuum pumps at both the Jamaica and Valley Stream ends. The line was evacuated to less than one millimeter which is comparable to values which have been obtained on individual sections.

Everest states that installation costs would be desirable. A breakdown of the installation costs was not available at the time of the preparation of the paper, and in general, this type of information is not included in AIEE presentations. Actual costs for installations in any particular locality cannot be taken as an example of costs for all areas since there is a wide variation in labor costs, installation conditions and underground structures. These data, however, will be available for presentation to other groups interested in the installation costs.

Main commented on the necessity for providing cathodic protection in addition to the external coating of the pipe. This protection is being provided as an additional safety factor since we do not desire corrosion to occur on any part of the pipe. Due to the territory through which the pipe passes, it is quite possible that external damage to the pipe coating may be caused by employees of other concerns, and this condition might not be brought to our attention until damage had occurred to the pipe. With the application of cathodic protection, the pipe would be protected from this type of damage.

Since the writing of this paper impedance and capacity tests have been made on the Jamaica–Valley Stream feeder. From these tests we are able to report the following unit values:

	Ohms Per Mile
Positive sequence impedance from data obtained during the heat run	$R_1 = 0.21$ $X_1 = 0.33$
Zero sequence impedance from zero sequence test with 212 amperes per phase made after conclusion of heat run	$R_0 = 1.31$ $X_0 = 0.45$
Zero sequence impedance calculated from line to ground and line to line tests made after conclusion of heat run	$R_0 = 1.28$ $X_0 = 0.54$

It is believed that the zero sequence reactance calculated from the line-to-ground and line-to-line tests is probably more nearly correct than the value obtained from the zero sequence test since the latter is a difference of two relatively large numbers. The difference in the resistance values given may be accounted for by the fact that the conductor temperature was somewhat higher in the zero sequence test.

Tests have been made of line capacitance in order to calculate charging current. The value obtained is 0.386 microfarad per mile per conductor. Discussion of the comments pertaining to the design and manufacture of the cable and accessories was covered by Del Mar and Wyatt.

115-Kv High-Pressure Oil-Filled Pipe Cable Installation at New Orleans, La.

W. R. BULLARD
MEMBER AIEE

A. D. PETTEE
FELLOW AIEE

G. L. RHODES
ASSOCIATE AIEE

Synopsis: The first major high-voltage pipe-type installation in this country to be installed without any stop joints and the first in which the cable was shipped without the conventional temporary lead sheath was placed in service in June 1947, by New Orleans Public Service, Incorporated. This installation incorporates a number of novel design features of unusual interest. The paper describes these features and reports the results of tests and studies carried out in connection with this installation.

THIS PAPER describes an installation of approximately 11 miles of 115-kv high pressure oil-filled pipe-type cable forming a portion of transmission loop within the city limits of New Orleans, Louisiana. The remainder of the loop consists of overhead steel tower line, 3.8 miles of which are of double-circuit construction and 4.9 miles, single circuit. Figure 1 shows the location of the underground line (dashed) with respect to surrounding geography and other main elements of the transmission and power supply system.

The underground installation is in two sections, one approximately $6^1/_4$ miles in length connecting Market Street steam electric generating station with Midtown stepdown transformer substation, which was a companion construction project. The other section, approximately $4^3/_4$ miles in length, extends from this substation to the double circuit section of the overhead line and continues as one circuit of the latter to the newly constructed Industrial Canal steam electric generating station. The other circuit of the double circuit overhead line returning to the juncture, continues over another route to Market Street steam electric station. An overhead 115-kv line to Gulfport, Mississippi, which forms an interconnection with electric service companies to the east, also terminates at the Industrial canal station. All lines entering the Market street station, other than the loop terminals mentioned, are of lower voltage and in underground cable, although there is an overhead 115-kv line extending north along the west bank of the Mississippi River which forms an interconnection with companies to the north. This connects to the Market Street station through 13.2-kv submarine cables, the transformation being located across the river.

For the entire installation the terrain is very flat. The soil is mostly loam and clay and in general relatively wet, the general elevation of New Orleans being such that removal of all drainage is done by pumping.

System Design

Selection of Cable Type

In determining the most desirable type of cable for this installation, consideration was given to operating records of cables of various types in this country of the voltage class involved, and to local experience in New Orleans with high-pressure gas pipe lines. Up to the time of study there had been a complete absence of inherent cable failures in service in existing low-pressure oil-filled and high-pressure pipe-type cable installations. There had been a few splice failures, and numerous oil leaks in oil-filled cable installations. The local experience with high-pressure gas pipe lines had been practically perfect insofar as durability and resistance to mechanical damage and to development of leaks were concerned, notwithstanding an inherent mechanical instability of the soil, that is, its tendency to shift under change of pressure, temperature and moisture content and to produce corresponding strains in underground structures.

It was evident that a high-pressure oil-filled pipe type of installation was particularly suited to local conditions and that if carefully engineered and installed, the underground portions could be made practically free from maintenance problems and dependable for a high degree of service continuity without the usual somewhat elaborate provisions for quick access to the lines, for pressure sectionalization, and the like, in case of failure. Accordingly, inherent strength plus simplicity were adopted as the keynotes of design, placing emphasis on quality of materials and workmanship and minimizing expenditures for underground facilities for rapid location and repair of pipe or cable failure.

Pipe

In accordance with the foregoing approach, somewhat stronger pipe was used than that corresponding to current practice—$6^5/_8$ inch outside diameter, 3/8 inch-wall thickness seamless pipe made of Grade *A* open-hearth steel, in double random lengths, weighing 25.03 pounds per foot. To simplify fabrication, thereby

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W. R. Bullard is electrical engineer with Ebasco Services, Inc., New York, N. Y., A. D. Pettee, is chief engineer of the General Cable Corporation, New York, N. Y., and G. L. Rhodes is planning engineer with the New Orleans Public Service Incorporated, New Orleans, La.

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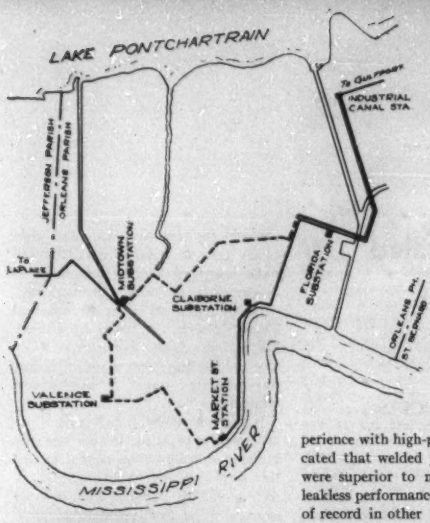

Figure 1. 115-kv transmission system

Valence & Claiborne substations not included in 110 kv system at present

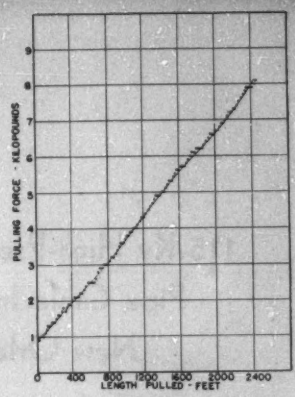

Figure 2. Variation in cable pulling force with length of cable pulled

2,500-foot length

facilitating delivery, simple beveled ends for butt welding were specified. Experiments had indicated that, with the proper technique, satisfactory butt welds could be made without the formation of internal icicles, and as an alternative it was known that external sleeves could be used. In actual construction the latter method of joining was adopted to minimize the need for higher than average care and skill in welding.

For protection of the pipe against corrosion, a coating composed of an asbestos felt-coal tar enamel combination was selected, largely because of favorable local experience over a number of years with this type of covering on high-pressure gas lines. However, in view of the higher temperatures involved in the operation of the cable installation, an enamel of higher melting temperature than that of the type commonly used on gas lines was specified.

To insure a smooth clean interior, the inner surface was wire-brushed with rotary brushes and blown out with compressed air (also swabbed if necessary) then coated with a rust inhibiting enamel that was known to have relatively low coefficient of friction in contact with the copper skid wires of the cable, and to have no significant deleterious effect on the insulating oil supplied by the cable manufacturer for filling the pipe. Both inside and outside coating were applied at the pipe mills and each length of pipe was capped for shipment.

Records of previous experience with pipe-type cable installations and local experience with high-pressure gas lines indicated that welded joints carefully made were superior to mechanical joints for leakless performance, practically all leaks of record in other installations investigated having been at gasketed connections and the like. Accordingly, insofar as was practicable, all-welded or brazed construction was employed. Joints at splices were specified to be welded. Joints in riser pipes and at the bases of potheads were required to be brazed. Weld-type joints were specified for control piping, and where weld-type fittings for such joints could not be obtained, supplementary brazing or soldering was employed insofar as it could be done without damaging operating parts.

Care was taken to locate the pipe so as to prevent accidental contact with other subsurface utilities, and electrical protection from corrosion was provided, using rectifiers, galvanic anodes, and connections to the railway negative system for potential control. Electrolysis surveys along the route of the line showed that, in certain areas adjacent to railway substations, electrical gradients were present in the earth of a magnitude sufficiently steep to preclude the use of galvanic anodes alone. At these substations, electrical drainage from the pipe to the railway negative bus was provided of a magnitude sufficient to hold the potential of the pipe somewhat below earth potential and at the same time co-ordinated with the potential of other electrically drained underground structures. At one location where the line passed under very low-potential street car rails, it was necessary to supply additional drainage by means of a small copper oxide rectifier. The fact that the pipe was welded throughout its 11 miles of length, with no insulated joints, created the problem of excessively negative pipe-to-soil potential in areas remote from the steep gradients near the substations. This was solved by placing resistance bonds between the coated steel pipe and the existing bare cast iron piping systems at strategic points, so as to raise the potential of the steel pipe adequately in these areas.

Sectionalization

In prior installations of pipe cable, semistop joints had been employed rather freely for purposes of pressure sectionalization under abnormal conditions, and for rapid location and repair of faults. Examination of designs available at the time revealed that the mechanical requirements of such joints might make it difficult to secure thoroughly dependable electrical characteristics, and while operating records showed no failures, it was felt that such joints represent a potential source of weakness. Also, piping and valve complications, that usually are incorporated in the design of semistop joints, seemed likely to be a source of maintenance expense. Methods of pressure sectionalization by freezing the oil in the pipe were investigated and found to be possible, although slower, of course, than by the use of semistop joints and valves.

Accordingly, in view of the likelihood of improving over-all electrical factors of safety and avoiding some maintenance expense, it was decided to avoid the use of semistop joints, and to accept the consequent necessity of using a slower procedure of restoring the line to service in the remote possibility of failure. Judging from existing experience records, and

considering the added security against failure provided by various measures of simplifying and strengthening design, the occurrence of pipe or cable failures should be very rare.

All splices therefore were of the straight-splice type with passages left fully open to flow of oil and with a minimum of special mechanical support and no anchoring of the spliced conductors, as described in another section of this paper.

Pulling Lengths

As mentioned previously, service failures in existing supertension cable lines at the time of study had been limited to failures of splices. This called attention to the inherently better control of quality of tape insulation under factory conditions of application, and indicated that the fewer the number of splices used in the line, the better would be the security against failures stemming from the personal element and other factors concerned with hand application of insulation in the field. Another incentive for holding the number of splices to a minimum, was the saving in cost. Accordingly it was decided to use pulling lengths as long as conservative design would allow. It was determined that a maximum length of straight pull of 2,500 feet would keep pulling tensions well within currently accepted allowable values of strain on the copper conductors. This value therefore was adopted as maximum, and splicing points were worked out with allowance for added tensions caused by bends in the pipe.

Attention was also given to sidewall pressures on the insulation caused by pulling around bends. Arbitrarily the allowable value of sidewall pressure was limited to the extremely conservative value of 100 pounds per foot of length. Splicing points were laid out and directions of pull were selected so as to keep bends principally at the feed end of pulling sections, thus minimizing the effect of the bends on pulling tension and sidewall pressure. The average actual pulling length as installed was about 2,000 feet and a very complete record of measured pulling tensions during installation indicated that the design was quite conservative. A maximum pulling length of 3,000 feet instead of 2,500 feet could have been used safely.

Typical pulling tension data are shown in Figure 2 for a 2,500-foot length and in Figure 3 for a 1,500-foot length that involved a number of bends. Table 1 shows the various lengths pulled, the equivalent straight length taking into account the effect of the bends, the estimated maximum tension, the actual tension, and the formula which was used to compute the estimated pulling force. A somewhat more refined formula, which was developed during the installation work, is also given.

Manholes

In the absence of semistop joints, and with all welded construction of the pipe and splice casings, there is little incentive to provide permanent manholes as a means of quick access to the line. With such construction all one sees in entering a permanent manhole is a pipe with an enlarged section containing the splice. In case of failure in the splice there would be some saving in time for restoration of service, but as explained later the splice was designed to have a breakdown strength greater than that of the cables. In the case of cable failure in a section between splices, the problem of withdrawing and replacing a 2,000-foot section of cable offers greater difficulties and involves the probability of consuming more time than the alternative of making an excavation at the fault and repairing it with a splice or with a short section of cable and two splices. Accordingly, it was decided to omit all permanent manholes, completely burying the line. This feature of construction is not novel, being very commonly and successfully used in Europe and South America, for cable of various types and voltage classes, and also used in this country in a few cases. Contrary to the natural conception of persons unfamiliar with such practice, location and repair of faults in buried-cable systems is not especially difficult, and faults are rarer than in conduit systems where cables in manholes are exposed to various types of damage. In the case of this installation, in addition to methods of location of electrical faults employed generally in buried cable systems, permanent test leads terminating in handholes in the sidewalk will be available for detecting or measuring pipe potential at a number of points along the line in connection with fault tracing current of various kinds. These test leads, however, were included in the design primarily for the purpose of checking potentials concerned with cathodic protection.

Table I. 115-Kv Cable

Comparison of Estimated and Actual Pulling Tensions*

Section	True Lengths L	Equivalent Length L^1	Estimated Maximum Tension	Actual Maximum Tension	Per Cent Error in Estimated Tension
1 -2	979			3,000	
2 -3	1,489	1,982	5,850	5,600	+ 4.5
3 -4	2,015	2,371	7,000	6,100	+14.8
4 -5	2,005	2,019	5,950	5,800	+ 2.6
5 -6	2,007	2,005	5,900	5,900	0
6 -7	1,987	2,003	5,900	6,050	− 2.5
7 -8	1,576	1,576	4,650	4,650	0
8 -9	1,560	1,751	5,170	4,400	+17.5
9 -10	1,830	1,996	5,900	6,000	− 1.7
10 -11	2,067	2,192	6,460	7,000	− 7.7
11 -12	1,511	1,811	5,350	4,900	+ 9.2
12 -13	2,165	2,256	6,650	6,850	− 2.9
13 -14	2,002	2,043	6,030	6,200	− 2.7
14 -15	2,301	2,363	6,970	7,300	− 4.5
15 -16	2,222	2,485	7,330	6,800	+ 7.8
16 -17	1,838	1,602	4,720	3,900	+21.0
17 -18	2,071	2,442	7,200	6,800	+ 5.9
18 -19	1,494			4,000	
19A-20	1,151			3,200	
20 -21	2,306	2,461	7,250	7,100	+ 2.1
21 -22	2,129	2,418	7,150	6,200	+15.3
22 -23	2,126	2,126	6,270	6,700	− 6.4
23 -24	1,713	1,858	5,480	5,200	+ 5.4
24 -25	1,684	1,753	5,180	5,100	+ 1.6
25 -26	2,148	2,225	6,550	6,600	− .8
26 -27	2,055	2,246	6,620	6,500	+ 1.8
27 -28	2,493	2,510	7,400	8,100	− 8.7
28 -29	2,277	2,510	7,400	7,000	+ 5.7
29 -30	2,106	2,465	7,250	6,600	+ 9.9
30 -31	1,667	1,921	5,660	5,600	+ 1.1
31 -32	1,985			5,450	

* This estimate based on a coefficient of friction of 0.30. Equivalent lengths calculated as follows:

$L^1 = Le^{ka} + ra$

L^1 = equivalent length at end of curve

k = coefficient of friction

r = radius of curve in feet

a = angle of curve in radians

L = tangent, or equivalent, length preceding curve

e = base of Naperian logarithm

Subsequent to the computation of the estimated maximum tensions shown, a refinement was made in the above formula to include the effect of the weight of the cable in the bend. The formula including this factor is:

$$L^1 = Le^{ka} + (r/k)\left(e^{\frac{Ka}{1}}\right)$$

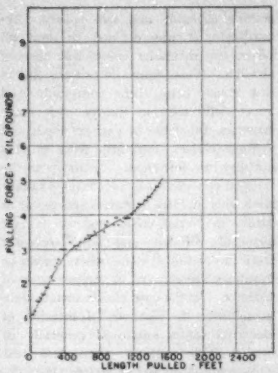

Figure 3. Variation in cable pulling force with length of cable pulled

1,500-foot length

Figure 4. Portable manhole

In lieu of permanent manholes, portable steel manholes were used for splicing as shown in Figure 4. These were approximately 7 feet deep, 8 feet long, and 6 feet wide and were fabricated in two sections which, when bolted together around the pipe, formed a water-tight enclosure.

Thermal Tests

The required rating of the circuit was 80,000 kva at 100 per cent load factor. The various manufacturers were requested to submit proposals of cable in pipe from which it was soon evident that different basic assumptions resulted in a wide range of recommended conductor size. It was, therefore, considered best to make local thermal tests to determine the thermal aspects of the different types of cable proposed and the proper conductor size. By mutual agreement with the cable manufacturers, 600,000 circular mil conductor was chosen with 480 mil insulation thickness as standard for this voltage class.

Five types of cable were tested, including four using nitrogen as the pressure medium and one using oil as the pressure medium. Two of those using gas pressure had permanent lead sheaths. Sample sections of 3-phase circuit of each of the five types were placed in a six inch pipe and joined end to end, forming one 3-phase circuit. This was short-circuited at one end and balanced 3-phase current was introduced at the other end. The sample sections at the two ends of the pipe were each 35 feet long and the others 25 feet each. The oil-pressure section of pipe was separated from the other sections by a micarta oil stop barrier. The pipe was coated with alternate layers of coal tar enamel and asbestos pipe line felt, giving an over-all thickness of 0.2 to 0.25 inch.

Temperature gradients from conductor to pipe were measured at five-foot intervals longitudinally, and the temperature gradient in the earth was measured at the middle of each section, all by means of thermocouples. In all, over 100 thermocouples were used.

The results indicated little if any difference between gas and oil, at 200-pound gauge pressure, as mediums for carrying away heat, although the insulation of the gas-pressure samples seemed slightly less thermally conductive than the oil-pressure sample. For two particular cables without lead sheaths, Figure 5 shows a reasonably typical comparison between gas-pressure and oil-pressure samples, with gradient indicated from conductor to earth 16 feet away from pipe.

Since the test results for the particular cable dimensions involved showed a noticeable increase of temperature rise for lead-sheathed cable as compared with those without lead sheaths, it was indicated rather clearly that permanent lead sheaths constitute a handicap from a thermal standpoint because of additional energy losses due to circulating currents.

It was concluded from the results that the required 80,000 kva continuous rating would be met adequately by 600,000 circular mil conductors with a maximum allowable copper temperature of 70 centigrade in the case of oil as a pressure medium if permanent lead sheaths were not used, it being felt that the soil at the test location was typical of that along the route in the actual installation, and that moisture content would ordinarily be high.

Cable Design and Manufacture

Cable Construction

The single conductor high pressure oil-filled pipe-type cable employed on this installation has the following construction:

Conductor

Area.....600,000 circular mil
Type.....Compack Round
Diameter.....0.813 inch

Shielding: 3—2.5 mil by 0.75 inch metallized paper tapes. (Included in the insulation wall thickness.)

Insulation

Total thickness 480 mils—3, 5 and 6.5 mil paper tapes, of graded density.

Shielding: 2—3.5 mil by 0.75 inch metallized paper tapes. (Included in the insulation wall thickness.)

Tape Moisture Seal

2—7 mil by 0.75 inch polyethylene tapes each intercalated with a 3 mil by 0.75 inch aluminum tape.

Outer Bedding

2—3 mil by 0.75 inch aluminum tapes applied butted.

Skid Wires

2—half-round copper wires 200 by 100 mils applied in open helix with approximately three-inch lay.

Following the application of the metallized paper tape shielding over the insulation, several temporary protective paper tapes were applied over-all and the cable was then impregnated in the conventional manner employed with solid-type cable with *HC* polybutene oil having a viscosity of about 800 Saybolt universal seconds at 100° Fahrenheit, or about one-fourth of that normally used on solid-type cable. After impregnation, the tape seal, the metal protective tape, and the skid wires were applied and the cable was placed on weathertight reels for shipment.

Tape Moisture Seal

Prior to the New Orleans installation, all high-pressure oil-filled and high-pressure gas-filled cable in commercial service in this country had been supplied with a temporary lead sheath which served to give protection against moisture entrance during shipment and which was stripped from the cable on installation. The ever-present hazard of flash floods in New Orleans and the obvious desirability of keeping to a minimum the field operations necessary during the cable pulling gave added impetus to a development already under way on a construction in which the temporary lead sheath is omitted and protection against moisture pickup during shipment is obtained by other means. Consideration was first given to the substitution of an extruded or taped nonmetallic jacket for the conventional temporary lead sheath. This approach was discarded promptly because, in spite of the elimination of lead, there still remained an important disadvantage in that complete removal or frequent perforation of the jacket would be necessary at time of installation in order to secure substantially complete and unimpeded access of the oil pressure to all parts of the cable surface beneath the jacket. Any departure from this basic, established design principle would have introduced factors requiring long time full-scale field trials for their substantiation. It was apparent that what was needed was a type of seal which would provide effective protection during shipment and during the short time the cable was exposed in the field but which, when subjected to the high pressure employed in operation, would not noticeably impede the cyclic interchange of oil between the cable structure and the interior of pipe.

A tape seal consisting of two layers of polyethylene tape each intercalated with a suitable thickness of metal tape was found to give the desired result. The effectiveness of this seal results from a gasketing action between the intercalated metal and polyethylene tapes. Polyethylene was chosen for the purpose because it is inherently water repellent, has a very low rate of moisture absorption and diffusion, and has no harmful effects on insulating oil even though, in time, it may tend to disintegrate and dissolve in the oil at the temperatures encountered in service.

The ability of the seal to provide the degree of protection necessary has been convincingly demonstrated by numerous laboratory tests and by the experience on the commercial shipments made to New Orleans. In the laboratory, samples from lengths of cable protected by the tape seal were bent on a 48-inch diameter drum, secured in this bent position, and then exposed to an atmosphere of a 100 per cent relative humidity. During the course of the tests, samples were removed periodically for radial power factor measurements. Any moisture penetration of the tape seal either by way of the gasketed zigzag path between tape surfaces or directly through the polyethylene tapes themselves would produce a progressive increase of the power factor of the outer insulating tapes of successive samples. Results of a typical test on the seal described before are shown in Figure 6. It will be noted that the penetration of water vapor after an exposure period of as long as five months at 100 per cent relative humidity and room temperature was not sufficient to produce any significant change in the 80 degree centigrade power factor of the insulating tapes. Similar results were also obtained on samples in which the temperature was permitted to vary cyclically in a 100 per cent relative humidity atmosphere by as much as 25 degree centigrade above and below room temperature. In this connection it is pertinent to note that Piper[1] has recently reported that several months exposure to Detroit's humid summer atmosphere (out of direct contact with rain) of cable samples protected by metallized paper tape resulted in no apparent increase in the moisture content of the insulation. Piper considers that metallized paper tape alone may be sufficient to bar the diffusion of water vapor into the insulation. These results have been confirmed in a preliminary manner on cable samples protected by two metallized paper tapes and two metal tapes without the polyethylene tape. Several such samples have been exposed to 100 per cent relative humidity and room temperature for times as long as four months without change in radial power factor. This evidence tends to indicate that the seal construction as used on the New Orleans cable is conservative, and for future installations consideration should be given to the omission of at least one of the polyethylene-metal tape seal layers.

The tape seal does not alter the operating characteristics of the cable in service. This point has been verified beyond question of doubt by numerous radial-flow resistance tests carried out at high-pressure on cable lengths with and without the tape seal. These tests have shown that upon the first application of pressure the resistance to inward radial flow through the insulation is the same with or without the tape seal. After several load cycles this holds true for either direction of flow.

The development work on the tape seal, as well as the investigation by Piper, also leads to the conclusion that even when a temporary lead sheath is employed, the use of fibrous material, such as cotton or plain paper tapes, outside of the metal-

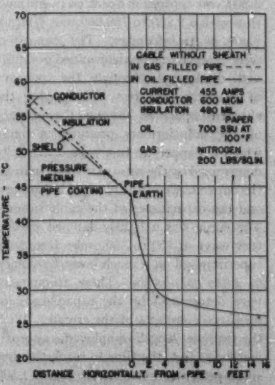

Figure 5. Comparison of conductor-earth temperature gradient for gas and oil cable samples

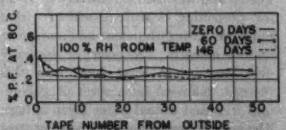

Figure 6. Typical radial power factor test on cable samples protected by tape moisture seal

Samples exposed to 100 per cent relative humidity and room temperature

Table II. Impulse Tests of Joints for 115-Kv High-Pressure Oil-Filled Cable

Reference Number	Hand Applied Radial Thickness Mils*	Longitudinal Creepage Inches	Stress Cone Length Inches	Breakdown				
				Location	Voltage Kv (Crest)	Average*** Stress Volts Per Mil	Maximum*** Stress Volts Per Mil	Longitudinal Stress Volts Per Mil
3	480	18.5		Joint	660**	1,375	2,050	35.5
5B	1,000	22.5	8.3	Cable	897	1,870	2,700	39.8
39/48	1,000	22.5	8.3	Cable	950	1,975	2,940	42.2

* As measured over the conductor.

** Failure occurred on 19th shot at 660 kv. Failure was partly radial and partly along "pencil."

*** In terms of the stress in the 480 mils of factory applied cable insulation except in case of joint number 3 where stresses apply to both factory and hand applied thicknesses.

Table III. 60-Cycle Tests of Joints for 115-Kv High-Pressure Oil-Filled Cable

Reference Number	Hand Applied Radial Thickness Mils*	Longitudinal Creepage Inches	Stress Cone Length Inches	Breakdown			
				Location	Voltage Kv (RMS)	Average** Stress Volts Per Mil	Maximum** Stress Volts Per Mil
4	720	18.6	4.3	Joint	454	946	1,410
5A	1,000	22.5	8.3	Cable	474	988	1,470
64/70	1,000	22.5	8.3	Cable	514	1,070	1,600

* As measured over the conductor.

** In terms of the stress in the 480 mils of factory applied cable insulation.

lized paper tape shielding over the insulation is undesirable since such materials become saturated very quickly under humid conditions and subsequent removal of such moisture is difficult.

Weather-Tight Reel

With a tape-seal type of construction, or in fact with any type of covering which is not removed at time of installation, some precautionary measures are advisable, if not essential, to prevent water or moisture collecting on the surface of the cable because even minute amounts of surface moisture can materially increase the time and expense of conditioning, once the cable is in place in the pipe. In the case of the New Orleans cable, protection from mechanical injury and surface moisture was obtained by means of a weather-tight reel. The reel was of steel construction with all seams carefully welded. The outer layer of cable was covered by a "blanket" of metallized cloth tightly sealed to the inside of the reel flange. The material used was one developed during the war for the Armed Forces and was used extensively by them for protecting delicate machinery from moisture during shipment and storage in humid climates. A layer of specially dried wood lags was placed over this blanket, followed by a sheet of galvanized iron and a final outer layer of lags. The ends of the lags and the joint at steel rim of the reel flange were further sealed with waterproofing paper and compound. Each reel was also equipped with a pocket containing silica gel and prior to shipment was flushed with dry nitrogen gas.

Impulse Design Tests

In order to obtain as great an impulse strength as is practical, two constructions were made up experimentally prior to start of manufacture. In the first of these the thinnest tape employed was 5 mils whereas in the second the thinnest tape was 3 mils. Since a lightning-surge generator was not readily available, a novel type of switching surge generator was employed for testing. Essentially the apparatus consisted of 16–70 microfarad 3,800-volt capacitors operated in conjunction with a 2,200 to 750,000-volt 60-cycle bank of transformers. The capacitors were charged to a predetermined potential and discharged through the primary of the transformers. Discharging the charged capacitors through the primary of the transformers produces a surge which is stepped up to a value essentially determined by the ratio of transformation, the capacitance of the charged capacitors, and the capacitance of the cable sample. The surge was a lightly-damped oscillatory wave of 150 to 350 cycles frequency superimposed on a much lower frequency of 5 to 20 cycles. These frequencies were determined by the capacitance inductance constants of the circuit. With the sample length employed, approximately 33 feet, the time to reach the first surge peak was between 1,400 and 1,500 microseconds. The voltage was determined from an oscillogram obtained with a cathode-ray oscilloscope. The amplitude of the oscillogram was calibrated in terms of voltage by means of a standard 75-centimeter sphere gap.

The normal test schedule consisted of applying 25 shots at 550 kv and 2 shots at each 50 kv increment until failure occurred.

The following tabulation gives a summary of typical results obtained on the two cable constructions:

Sample Number	Surge Breakdown Kv (Crest) Minimum Tape Thickness 3 Mil	5 Mil
1	890	915
2	960	835
3	922	858
Average	924	869
Average stress (volts per mil)	1,925	1,810
Average maximum stress (volts per mil)	2,870	2,700

These values were all very high in relation to the recommended withstand stress of 1,200 volts per mil and the 3-mil construction had a small advantage. Because of these factors and because of the requirement that the line have the greatest inherent strength, the 3-mil construction was adopted for the cable as actually built.

Accessory Design

Normal Joint

In view of the decision to eliminate permanent manholes, it became desirable to insure that the joint design used would be as strong electrically as the cable itself on both normal frequency and impulse. For this reason design tests were conducted to determine the necessary creepage path length, hand applied tape thickness, and so forth.

The joint design adopted is shown in Figure 7*A*. A flush-type copper connector having the same diameter as the conductor was used to avoid the high stress region which occurs near the ends of nonflush connectors and requires special treatment. The connector was a soldered-type supplemented by pressed indents (Figure 7*B*). Long pencils were made on the factory insulation by tearing each tape individually at points approximately 0.16 inch apart. Paper tapes of the same thickness and density as those removed, were applied butt edge to match the pencil on the factory applied insulation. The hand-applied taping was then built up until the thickness was 1.0 inch, or 108 per cent greater than that of the factory-applied insulation. The hand-applied build-up was tapered at the ends

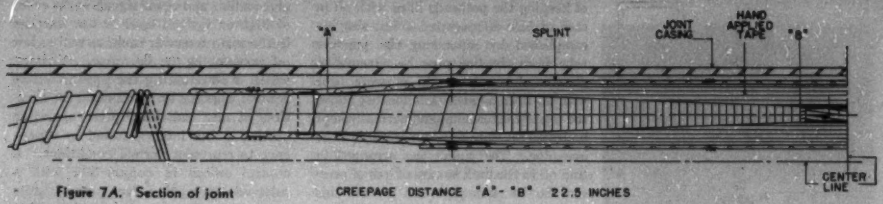

Figure 7A. Section of joint

Creepage distance "A"–"B" . . . . 22.5 inches
Length, pencil . . . . . 14.3 inches
Thickness of hand applied tape over conductor . . . . . 1.0 inch

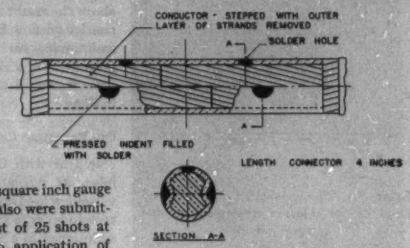

Figure 7B. Joint connector

of the joints by tearing the successive tapes at points approximately 0.10 inch apart. The joint was completely shielded with tinsel copper braid. Polyethylene tape was temporarily applied over the completely insulated and shielded joint to protect it during construction. After all three joints were completed, the polyethylene tape was removed, splints consisting of split laminated phenolic tubing were bound in place over the individual phases, and the three splices were brought together so as to permit the use of an 8¾ inch outside diameter pipe casing. The use of splints permitted shifting the position of the completed individual joints without risk of disturbing the hand-applied insulation and in service will eliminate any possibility of buckling at the splice location as the result of compressive forces that may be set up while the cable is under load. The pipe casing was split longitudinally into two parts so as to keep the length of the temporary manhole to a minimum. The circumferential and longitudinal seams in the casing were then welded, using an electric arc.

Table II shows the essential design dimensions and impulse test results on several of the experimental joints tested. All joints were tested at 200 pounds per square inch gauge. Joint number 3 had no preliminary 60-cycle test but both of the other joints were tested for six hours at 163 kv (340 volts per mil) and six hours at 193 kv (402 volt per mil) before being tested on surge. The schedule followed on the surge tests consisted of 25 shots at 660 kv followed by one or more shots at each 50-kv step to failure. It will be noted that joints 58 and 39/48, both of which have dimensions similar to those employed in the actual installation, have impulse strengths in excess of that of the cable.

Table III shows design data and 60-cycle test results on additional experimental joints. These joints also were tested at 200 pounds per square inch gauge oil pressure. All joints also were submitted to a preliminary test of 25 shots at 660-kv impulse prior to application of normal frequency.

The Association of Edison Illuminating Companies' (AEIC) 60-cycle test schedule for the cable insulation thickness in question is shown by the dotted line in Figure 8. Except for a difference in the initial step (6 hours at 402 instead of 340 volts per mil), the schedule used was the same as the AEIC schedule up to the middle of the fourth step (588 volts per mil). Thereafter the voltage was raised at a substantially geometric rate corresponding to an increase of 20 per cent in 3 hours.

The use of a geometric rate of rise over the latter and significant portion of the test schedule up to the point of failure makes it possible to obtain a simple, direct and relatively accurate comparison on the basis of the final voltage itself and avoids the need for evaluation on a one-hour equivalent basis by using an arbitrary power law. Where the voltage is raised in large steps, or the test is made for a long time at relatively low stress, comparison between two tests can be made only by calculation with the use of an arbitrary power law.

Some previously published results on 60-cycle breakdown tests on high-pressure oil-filled cable are not comparable with the foregoing 60-cycle test results. For example, a one-hour equivalent value of 1,200 volts per mil obtained by evaluation of a test made for many hours at a stress of 400 to 600 volts per mil gives no indication whatsoever as to the actual one-hour strength of the cable. As a case in point, Atkinson[3] has reported tests on identical samples of ordinary "solid" cable and has shown that by merely extending the time of test into the range where the exponential law does not apply, it is possible to obtain equivalent values of as much as 1,200 volts per mil for a solid cable on which breakdown would actually occur in one hour at about half this stress.

Terminals

The terminal has an internal laminated bakelite pressure tube designed to withstand 200 pounds per square inch continuously. The pressure tube is sealed to the metal shells at each end with long tapered baked cemented joints, backed up by rugged stuffing glands. All terminal pressure tubes were tested to 400 pounds per square inch for four hours before shipment. The space between pressure tube and factory applied insulation is filled by a saturated paper tube.

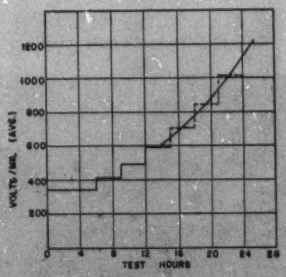

Figure 8. Voltage test schedule for 60-cycle tests

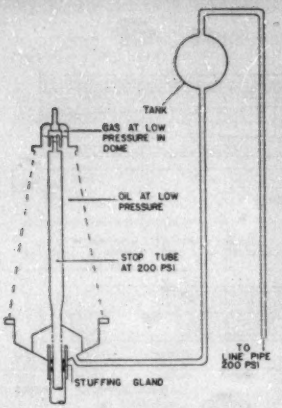

Figure 9. Arrangement for stop tube oil supply in case of loss of pressure and oil from adjacent pipe line

Porcelain insulators are standard 138-kv bushings with withstand impulse of 650 kv and 60-cycle ratings of 335-kv dry and 275-kv wet. Co-ordinating gaps are set for 450-kv full-wave flashover.

The space between the porcelain insulator and the pressure tube is filled with cable oil and maintained at low pressure by gas in the dome. The porcelain insulator is protected against high-pressure leaks by a check valve. Expansion between conductor and the top of the pressure tube is accommodated by a slip joint, while the relative expansion between pressure tube assembly and the protective hood is accommodated by flexible straps.

In an existing pipe-type cable installation, a pothead failure, understood to have been caused by loss of pressure followed by draining of the oil from the pressure element of the pothead, had called attention to the need for some positive means of keeping the potheads filled with oil in case of loss of pressure. This was accomplished by separating the pressure tube from the line pipe by a semistop gland and supplying the pressure tube with oil from a small tank located above the top of the terminal and normally kept under oil pressure by a connection to the line pipe. The piping was arranged to trap oil in the tank in case of loss of pressure in the main pipe and to feed this trapped oil into the potheads at the base above the semistop gland which served to restrict oil flow back into the pipe, thus maintaining the pothead pressure tube full of oil for a considerable time. This arrangement is shown diagrammatically in Figure 9.

Oil-Pressure Control System

In line with the field design goal of inherent strength and dependability, vacuum is used above the oil in the storage tank in order to insure that the oil will be maintained in a well degassed condition, thus minimizing the possibility of gas bubbles in the insulation under low pressure or loss of pressure conditions.

Two oil-pressure control units are installed at Midtown substation to maintain a pressure of approximately 200 pounds per square inch gauge on the oil in the line pipe. Normally each unit supplies one of the two circuits. However, the units are so designed that either unit is capable of supplying both lines. Furthermore, paralleling valves are provided so that any main component of either unit may be used with the other. Each unit consists essentially of a pressure pump with associated check relief and solenoid operated valves, a 1,300-gallon reservoir tank mounted on the substation structure, and a vacuum pump for maintaining the oil in the reservoir under vacuum. A recording and indicating pressure gauge provides a permanent record of system pressures. Alarm relays are provided to give audible and visual signals in the event of high or low oil level or low vacuum in the main reservoir tank, as well as low oil pressure in the line pipe. A timer, which is actuated by the pressure-pump control, operates an alarm when the duration of any individual pump operation exceeds a time interval found by experience to represent normal conditions. A control switch in conjunction with a solenoid operated valve automatically controls the oil pressure variations within predetermined limits. All valves, stems, packing glands, and so forth, on the low-pressure side of the system are maintained under a small positive pressure provided by a gravity head, so as to eliminate any possibility of air or moisture entrance. To protect riser pipe and pothead stop tubes, relief diaphragms are provided, discharging into small evacuated reservoirs.

Field Construction

Subsurface Conditions

The route of the cable installation was through a congested urban area, in city streets with various types of pavement, such as gravel, brick top, 8 inch concrete and, in one location, heavy granite blocks with concrete topping. In some instances the route was under paved sidewalks or in ground with sod covering. The city, being below river level, has a very large surface drainage system of canals and pumping stations. It was necessary to cross the canals, in several instances, either by going above grade, as shown in Figure 10, or in event the canal was covered over, going either through or under it. It was necessary, in passing through canals, to weld an outer casing pipe to the carrier pipe, extending several feet beyond the canal well on each side. The space between the carrier and casing was filled with oil for thermal conductivity and vented to atmosphere for safety.

Figure 10. Overhead crossing at canal

Figure 11 (right). Cable feeding rig

The entire route was thoroughly explored with probe rods and where necessary an excavation was made to determine obstructions, providing information from which the final grade and radius of bends of the pipe were determined.

Pipe Laying, Welding, and Conditioning

The pipe was installed at depths varying from two feet in crossing of some drainage canals, to 14 feet in crossing under others; average depth being approximately four feet. The pipe was prebent locally, using cold bending for all bends of radii from 33 to 200 feet. No bends of shorter radius than 33 feet were used. All bends of radius greater than 200 feet are within the natural flexibility of the pipe obtainable during installation.

Pipe was welded above grade, generally to 300-foot lengths, thereby reducing the welding in the trench. All welds were tested to 500 pounds pressure, using a special testing device, designed and constructed locally, to build up pressure over approximately two-foot lengths of pipe each side of weld. A special water trough, fitting around the pipe and submerging the weld, was used for testing leaks, pressure being maintained over approximately 1/4 hour periods. Pipe sections were completed and at temporary pulling manholes, sealed off and filled with dry nitrogen until time for cable pulling. The pipe was exposed to the atmosphere, in some instances, several months prior to installation in the ground. No significant rusting was evidenced. At welding points, the protective interior coating was charred and consumed, leaving a bright surface on the interior. The dry nitrogen atmosphere preserved this surface until time for cable pulling. Sudden storms resulted in flooding two of the pipe sections during installation. These sections were first swabbed, then dry air was blown through the pipe until the dew point was down to below −20 Fahrenheit. Dry air was obtained by compressing air to 500 pounds, passing it through a heat exchanger, lowering the temperature to 32 degrees, then reexpanding. Pipe coating was tested prior to installation, using a small-capacity high-voltage test set connected to a spiral doughnut of wire fitting around and rolled along the pipe. Any pin holes or defects found were patched. The test voltage was approximately 26 kv. Damage to the coating from poor backfill was located by using battery supply between pipe and ground and exploratory metallic probes in the soil above and along the pipe, these being connected to a vacuum tube voltmeter. Where necessary, reexcavation and patching of coating was done.

Figure 12. Temporary baffle used in splicing

Pulling

Due to the occurrence of sudden severe storms, it was considered advisable to arrange for cable installation in such a way that no damage could result from this cause. The equipment, therefore, was designed and fabricated to permit feeding of the cable from the reels in a protective housing above grade, through a goose-neck pipe connection into the pipe in the ground. This is shown in Figure 11. Approximately five gallons of cable oil were poured into the pipe before the cable was pulled. The pulling rig had a gasoline engine powered fluid drive, with a steel boom extending horizontally over the manhole, connecting to a watertight enclosed sheave in the manhole. A sheave at the manhole end of the boom was linked with a dynamometer arranged to give continuous indication of pulling tension. The sheave diameter was designed so that, with a Veeder revolution counter the length already pulled was continuously indicated as pulling progressed. Dry air was supplied to the pipe during the pulling operation. Temporary phone communication was provided for the pulling and feeding manholes, and the pulling rate was held normally to about 25 feet per minute. It was varied from approximately 15 to 30 feet per minute during one pull to determine the effect on the pulling tension, and no considerable variation was noted. Several pulls were made during storm conditions without any indication of high humidity in the pipe. Prior to pulling cable, a cleaning plug was pulled or blown through the pipe. On the first few sections the plug

Figure 13. Pothead in process

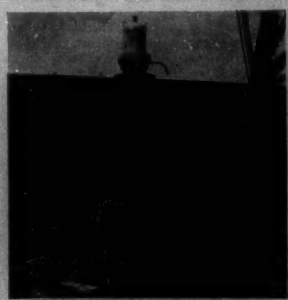

Figure 14. Completed pothead

was followed by a vacuum cleaner. However, the residue picked up by the latter was insufficient to justify its further use. The plug was blown through the pipe with approximately fifteen pounds air pressure, with a piano wire attached. This piano wire had permanent markers soldered to it and was used as a measuring chain for a final check of the surveyed length before fixing the cutting lengths of cable. The wire was used also to draw in a 1/4-inch steel rope which served as a leader for the 3/4-inch steel pulling rope. The foreign material removed from the pipe was primarily interior coating ash left from the pipe welding process.

After the pulling operation was completed, the pipe sections were temporarily sealed with night caps until splicing could be completed.

Splicing and Terminating

The portable steel manholes, at times, were subjected to exterior flooding from storms during splicing. Humidity was controlled by a continuous supply of dry air from the air compressors which had been used in drying out the pipe. A temporary baffle plate, Figure 12, was used for sealing the pipe sections to retain an atmosphere of dry nitrogen in the pipe during splicing. The splicing was done on a three-shift 24-hour basis requiring approximately three days for a 3-conductor joint. An additional 12 hours was required, per joint, for welding the split joint casing. Tests were made of temperatures involved during welding of the split casing, and the rate was controlled so that no excessive temperature of the cable resulted.

A trifurcating head, consisting of a plate, welded to a short piece of 8 5/8 inch steel pipe, and with three copper tubes brazed to and projecting from the plate, was used for leading the conductors from the line pipe to their respective potheads. The copper tubing was protected with the same type of coating as the line pipe. Figures 13 and 14 show one of the potheads in process of assembly in a protective house, and the completed job.

Oil-Filling

After the splice and pothead installations were completed, the nitrogen was removed from the system by application of vacuum until a pressure of 2 millimeters absolute was reached. Pressures were checked at the two ends and middle of the line sections and vacuum was held without pumping for a number of hours without significant rise in pressure. Preheated oil was drawn from the tank cars, filtered and degassed by spraying into the reservoir tank under vacuum. The oil was then introduced into the pipe, requiring a period of several days for each of the two sections. The initial oil drawn from the remote end of the pipe came out clear, with only a slight amount of interior coating dust in evidence. Dielectric strength and power factor tests on the oil before and after introduction into the system were substantially the same.

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1. Moisture Equilibrium Between Gas Space and Fibrous Materials in Enclosed Electric Equipment, John D. Piper. AIEE Transactions, volume 65, 1946, pages 791–7.

2. Dielectric Strength as Related to Time. R. W. Atkinson. *Minutes*, Edison Electric Institute Transmission and Distribution Committee Meeting, Pittsburgh, Pa., February 1943, page 28.

Discussion

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): Twenty years ago, the AIEE had presented to it a symposium of papers covering the initial commercial installations of oil-filled underground power cable. These installations symbolized cable's coming of age and they were based on developments originated by the Societa Italiana Pirelli. This and another pipe-type cable paper[1], together with work done by some other manufacturers on extra-high voltage cable systems in this country, reflect great credit on the American cable manufacturing industry. The encouragement and ideas of cable users have been of some help in such developments.

For operation at 69 kv, there is a choice of many types of cable systems, including solid-type or oil-filled cable either in ducts or buried, medium-gas-pressure or self-contained high-pressure cable, and an assortment of pipe-types. Altogether, there may be 15 types at 69 kv, about 12 types at 138 kv, and an unknown, but smaller number, of types at 230 kv. My point now is that one should not become enamored of any one type, but should approach each situation on a straight engineering basis. As experience develops, however, perhaps a few of the many types will drop out of the picture.

Of course, the simplest installation is of solid-type cable with sealed joints filled with compound and having no oil reservoirs. Such an installation of 69-kv cable was made in ducts in Chicago over five years ago and a similar, but buried, installation is to be made in Winnipeg this year.

A line of low-pressure oil-filled cable should require very little maintenance according to operating experience of recent years. Also, with such cables there is no problem of iron pipe losses which is a factor militating against the use of pipe-type cable systems for large currents. It would be interesting to learn the manufacturer's ideas as to the approximate limiting current that is feasible with a pipe-type system.

In the pipe-type cable installations, there is the possibility of between-phase failures. This means a higher interrupting duty for the existing or required new circuit breakers, or both, than for installations of single-conductor solid-type or oil-filled cable. This higher duty has militated against the use of pipe-type cable in at least one instance.

In some cases, the fact that lead-sheathed cable of any type may be installed in existing or contemplated conduit systems (that may contain other cables) makes the use of pipe-type cable systems economically unattractive.

Extensive field and laboratory tests, running up to 70,000 hours and up to 140 degrees centigrade, on cable samples, supplemented by various test data obtained on paper tapes in oil, have indicated that oil-filled cable may safely operate at higher than the standard values used in this country for oil-filled pipe-type cables. This advantage amounts to about 10 degrees centigrade for normal operation and about 20 degrees centigrade for emergency operation. Perhaps, with more test data and some operating experience, higher operating temperatures than the standard values may also be established for some of the pipe-type cable systems. It would be of interest, for example, to learn what would happen to a 69-kv polyethylene taped cable if its conductor were to operate for a week or two up to 115 degrees during an emergency.

Also, it would be helpful to learn the manufacturer's present ideas of the limiting temperature for say, 4,000 hours per year for 40 years, to which polyethylene may be subjected when immersed in oil, as described in the paper. At such temperatures, what is the expected condition of the polyethylene at the end of the assumed 40-year period?

This paper and other information have disclosed some marvelous 60-cycle breakdown test data as obtained in tests taking hours. Such data would indicate that substantial reductions in required insulation thicknesses are in order, especially for some of the pipe-type systems. However, as has been known for some time, the criterion to use in most cases is the unit impulse strength of the insulation, and this criterion has not disclosed any particular advantage for the pipe-type systems.

In general, it seems that the pipe-type cable systems will be the most suitable and economical for some conditions for operation at extra high voltages, but the nature and extent of these conditions are still being defined.

Reference

1. 138,000 Volt Polyethylene-Sheathed Compression Cable—Pipe Line Type, J. E. McCormack, C. T. Hatcher, E. S. Wyatt, W. A. DelMar, E. J. Merrell, J. H. Palmer, E. F. DeTurk. AIEE Transactions, volume 67, 1948, pages 447-74.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): The paper by Bullard, Pettee and Rhodes describes an interesting step in the development of pipe-type cables, namely the shipment of insulated cable without a protective lead sheath and the consequent elimination of the lead stripping operations in the field.

Protection is obtained by the use of polyethylene tapes intercalated with a metal tape. The authors state that the polyethylene tapes may disintegrate and dissolve in oil at the temperatures encountered in service. I would like to know what the temperatures are at which this condition will occur?

It is also stated that by blowing a "mouse" through the pipe for wiring the pipe it was found that some foreign material was removed. I would like to mention that we also found during the installations of our current pipe lines that the "mouse" was an effective means for removing any coating ash or welding residue from the pipe.

The authors write that a check of the actual length between manholes was made after the pipe was installed before the cable cutting length was specified. In our current installations it was necessary to specify the cutting lengths before the pipe was installed. The lengths were carefully checked from scale drawings and field surveys and the cutting lengths determined. This worked out satisfactorily, thereby determining that it is not necessary to wait for completion of the pipe.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): The paper by Bullard, Pettee and Rhodes is of great interest and value, since it describes the first installation of high-voltage cable where a lead sheath protective covering has been dispensed with entirely and an organic jacket has been substituted.

The best construction of polyethylene protective jacket requires careful thought and study. The one used in New Orleans included only two polyethylene tapes, intercalated with and backed up by metal tapes. There has been a good deal of experience with "two-tape" jackets and insulations and it is well known that a laminated multitape construction is required to eliminate, or rather to cover up all flaws and defects. It is accordingly plain that the metal tapes are relied upon more than the polyethylene tapes in this construction. Metal tapes will seal against entrance of water vapor into the insulation but are not a very reliable seal against entrance of free water and oxygen. The two polyethylene tapes must have been incorporated for this purpose, and to assure against any flaws or defects in these two tapes, special gas-tight shipping reels were used, including dryers and a neutral gas atmosphere. Provided these reels are not damaged during shipment, this furnishes adequate protection during long-time shipment exposure and also good protection during the shorter period of installation exposure.

The special gas-tight shipping reels, however, represent a very appreciable increase in cost as compared with the more ordinary shipping reels, and this raises a question whether it would not be sounder design practice to invest more of the total cost in a more rugged polyethylene jacket and a lesser cost in shipping reels without an attempt to make them gas-tight but weatherproofed sufficiently to prevent wet conditions in the rain. We have found that a multitape construction of this kind with intercalated polyethylene and metal tapes is more reliable and less costly than a relatively heavy extruded polyethylene jacket and requires about the same degree of protection during shipment.

It is interesting to note that all precautions were taken to eliminate impurities from the system in New Orleans. We fully agree that this is sound practice because the jacket cannot be depended upon during long years of service in sealing the insulation against any impurities that might be left in the pipe.

Such impurities have great affinity for impregnated paper insulation, and no type of organic jacket is permanently impervious to such impurities.

There is no sectionalization of any kind in this installation and this raises a question of whether the relatively small additional cost of semistop joints for sectionalization is not justified. Sectionalization of this kind offers a number of advantages. It allows prompt complete treatment and final filling of the system as the work progresses and thereby reduces the chances of absorbed impurities in the insulation, particularly at the joints. This also offers a chance to anchor the cable in the pipe to prevent a cumulative movement. Doubtless the extremely flat profile in New Orleans did not require this, but where rugged profile with long slopes is encountered anchoring of the cable is important.

One of the most important functions of this kind of sectionalization is to prevent contamination of long sections of the line in case of service failures and to facilitate future maintenance and repair work. Location of leaks is also greatly facilitated in this way. In deciding upon a system design these advantages must be weighed against the additional cost that they represent. All previous high-voltage cable installations in this country have had sectionalization of one kind or another because of the heavy investment involved, and its elimination represents a new departure and a great deal of faith in the integrity of the system.

Guy Everest (The Okonite Company, Passaic, N. J.): Was any consideration given to bell end pipe and use of chill rings or bell and spigot pipe, because both of these types would have been less expensive than the external sleeves for welding? External sleeves double the cost of welding compared to these types.

Corrosion Protection on Pipe

Was there any record of the pinholes or "holidays" in this coating? It has been our experience that the extruded type of corrosion protection is better to handle and has less "holidays" per length of pipe.

It appears the wrapped type of coating together with electrical protection from corrosion was used.

Was this decision to use both forms of protection based on local pipe experience or was the cost the ruling factor?

Sectionalization

Semistop joints have proved satisfactory in the field and long-time high-voltage tests on them have shown them to be equal in strength to the cable. Maintenance on the semistop joints is nil provided they are properly installed, and they facilitate installation and allow oil submersion of the cables shortly after they have been pulled in.

It appears that this installation has been designed so that the weakest point is the end termination.

It has been found that there is a difference of opinion among engineers over the country as to whether it is better to have a pothead failure, a joint failure, or a cable failure. Some prefer a pothead to fail because of the ease of replacement, provided suitable means are taken to prevent pieces of porcelain from damaging equipment close by. Others prefer cable failure because of the position, namely, underground, where least damage can occur to other equipment or the public. Others prefer a joint failure because of ease of repair and location, with possibility of damage to equipment or the public being remote. Consequently, this calls for different designs.

Thermal Tests

It is unfortunate a higher conductor temperature, say 70 degrees centigrade, was not obtained in these tests. It is known viscosity of the oil has a considerable effect on the thermal resistance of the oil zone. If the 70 degrees centigrade temperature had been obtained, a greater difference between oil and gas cable in favor of oil would have occurred.

Moisture Seal

We agree pipe-type cables can be shipped in sealed reels eliminating the use of lead sheaths by a suitable tape seal over the insulated cable.

Impulse Test

Can the method of impulse testing be compared to AIEE standards where a $1^1/_2 \times 40$ micro second wave is used? It is probably not comparable and therefore misleading. Can some clarification or test data be given to show the comparison?

Joints

So far as we are concerned it is imperative that joints can be tested similarly to the cable under Association of Edison Illuminating Companies' time tests and the two correlated so that joint is equal to or better than the cable. Our field experience in installations of 15 years' service shows this practice is satisfactory.

It would be interesting to know the total cost per mile of this installation and perhaps a breakdown of the manhours used for pipe installation, that is, welding, excavation, testing and cable installation on such as cable pulling, jointing, terminating, pipe evacuation and oil filling.

W. R. Bullard, A. D. Pettee, G. L. Rhodes: Shanklin has made some very appropriate comments on the tape seal. The effective-

ness of the seal is undoubtedly due to the multiplicity of tapes. Two metallized paper tapes, two pairs of intercalated metal and polyethylene tapes, and two metal tapes make a formidable barrier against air, moisture and water, without preventing free transfer of the fluid insulating medium. We have even immersed the bent cable in water for 120 hours at 50 degrees centigrade plus 16 hours at room temperature without detecting any significant change in the radial power factor.

Shanklin has misinterpreted our description of the weathertight reel. Reasonable effort was made to make it weathertight without excessive expense, but it is definitely not designed to hold pressure. A reel designed to stand as little as five pounds per square inch would run into too much unnecessary cost.

With regard to the absence of sectionalization, the advantages of semistop joints mentioned by Shanklin were recognized, but after careful study, they were considered to be of minor importance and outweighed by the disadvantages. As pointed out by Everest, such joints have not given trouble in service to date and the results of high-voltage tests have been valuable. However, it is obvious that in operation, mechanical stresses, due either to differential pressures in the pipe or due to expansion or contraction of the cables, are imparted to the cable insulation at the pressure seal.

Some recent tests and studies have revealed that considerable forces of tension or compression can be built up at points of cable anchorage due to load cycles and the like. These problems are being solved on other lines but they require the incorporation of special features in addition to the semistop gland which is designed primarily to check the flow of the filling medium.

An incidental advantage that has been found to accrue from the absence of semistop joints is that pressure control under normal operation is facilitated by the free flow of oil throughout the length of pipe, because there is an absence of restrictions to flow which are present in lines of conventional design with semistop joints and their small by-passes.

With respect to filling the pipe with oil, the method of filling from the end of the line after the cable was completely installed was found to be very successful at New Orleans.

Everest has asked whether the use of bell and chill ring or bell and spigot pipe was considered for the New Orleans installation. The selection of the external sleeve with its double weld was made to facilitate pipe delivery and does not indicate that the other types of pipe connections would not have been acceptable as to physical merit.

With respect to corrosion protection, as mentioned in the paper, pinholes or "holidays" in the coating were carefully located by electrical means, and patched. The wrapped type of coating was selected because of previous favorable local experience with this type. Cathodic protection was considered to be a desirable adjunct to the protection afforded by the coating and probably would have been used if any other type of coating had been selected, because it is believed that no presently practicable type of coating is completely immune to development of defects after installation, including of course punctures from external causes.

Everest has discussed the relative electrical strengths of the cable, joints, and terminals. For this installation, neither joint nor terminal was deliberately made the weakest link. Because the joints were made electrically stronger than the cable, and the impulse basic insulation level of the terminals (650 kv) is lower than that of the cable, the electrical strength of the terminal may be considered the weakest link. Further than this, the terminals and cable have been protected by co-ordinating gaps at the terminal insulator set for 450-kv full wave flashover.

Everest has mentioned the "known" effect of viscosity of the oil on the thermal resistance of the oil zone, and has regretted that the thermal tests were not carried on with a conductor temperature of 70 degrees centigrade. We do not have any quantitative comparative data on this point and it is unfortunate that Everest did not present data to indicate its relative importance. Data on this point with an oil having a viscosity of 700 Saybolt universal seconds at 100 Fahrenheit may be found in the Sanderson, Sticher and McGrath paper.[1]

In comment on his question as to the comparison between the standard AIEE $1^1/_2$x40 wave and the long wave of the switching-surge type that was used in testing the cable and joints, we do not have complete enough data to establish definitely this relationship, but from a study of all available published and unpublished data, including our own, we are led to the conclusion that the impulse strength as developed by the type of wave we used cannot be much different from the strength that would be developed by the $1^1/_2$x40 wave. Probably the former would not be higher.

It should be kept in mind that while a steep wave front has an enormous effect in changing the voltage distribution in tests on electrical equipment with wound coils, the steepness of the wave is of no such importance in a cable.

We expect in the near future to have definite comparative data on this point.

Halperin and Hatcher have asked for data on the softening or disintegration of polyethylene in hot oil. We have no data that would foretell the condition of polyethylene after 40 years of service. A two-day free suspension of clear polyethylene slabs (dumbbell shape) .075 inch thick, in a natural petroleum oil with a viscosity of 57 Saybolt universal seconds at 210 Fahrenheit (approximately 9.25 centistokes) gave the following results:

	Tensile Strength, Lbs Per Sq In.	Elongation, Per Cent
Original	1,450	700
After 2 days at 75 degrees centigrade (some swelling)	1,140	580
After 2 days at 80 degrees centigrade	Too soft to test	
After 2 days at 85 degrees centigrade	Dissolved	

Reference

1. Thermal Characteristics of a 120-Kv High-Pressure, Gas Filled Cable Installation, W. D. Sanderson, Joseph Sticher, M. H. McGrath, AIEE Transactions, volume 67, 1948, pages 487–502.

Thermal Characteristics of a 120-Kv High-Pressure, Gas-Filled Cable Installation

W. D. SANDERSON
MEMBER AIEE

JOSEPH STICHER
MEMBER AIEE

M. H. McGRATH
ASSOCIATE AIEE

SOME YEARS AGO The Detroit Edison Company was faced with the problem of procuring for its system, underground cable that was to be operated at 120 kv and was to be capable of transmitting 95,000 kva continuously.

There were only two types of cable that had been used in this country for voltages above 100 kv and for loads of this order, namely, the oil-filled cable (Pirelli-type) and the Oilostatic type of cable. The Detroit Edison Company was anxious, however, to give consideration to all high-voltage cable in commercial use here and abroad, but was unwilling to adopt a cable type that had no operating record for loads of this order, without first conducting considerable experimental work.

One such cable type of which there was no installation having the required capacity was the Hochstadter[1] pipe-type, gas-compression cable. It was decided to provide an experimental field installation of this type of cable in order to determine its behavior when designed for 95,000 kva at 120 kv and operated under various conditions that might occur in service operation. Also, the successful operation in England of the Beaver type[2] of high-pressure, gas-filled cable at 132 kv suggested the possibility of its use here, and a length of high-pressure, gas-filled cable was to be included in the experimental installation, but with the important modification that all three cables of the 3-phase line would be pulled into one steel pipe instead of employing individual lead pressure containers for each conductor as had been the custom up to that time for Beaver-type cable. This modified type of cable is referred to as the *SMD* type of cable in this paper.

One of the major reasons for the 1941 experimental field installation was to gain experience in the installation and the operation of gas-pressure, pipe-type cable; another was the procurement of experimental data concerning the electrical performance of the insulation, such as can be obtained from power factor measurements that are made under various test conditions; and still another reason was to determine the temperatures and temperature distributions obtaining in such a cable installation during various loading schedules. It is at once apparent that such an installation lends itself readily to a study of various kinds of other pipe-type cable that might be considered from time to time, as well as a study of the merits of modifications in existing types of pipe-line cable—for instance, the determination of the minimum insulation thickness that is practicable for certain conditions of service operation.

Some of the results and experiences gained with the experimental installation and with a commercial installation constructed subsequently were reported in two previous papers.[3,4] Little information was given in these papers concerning thermal characteristics of the cable installation, mainly because there was only a little information available at that time. The present paper, therefore, deals exclusively with the thermal characteristics, and with other phases only insofar as they have a direct bearing on or relation to such characteristics, in the hope that these results may be of aid in the design of pipe-type cable systems.

General Layout of the Experimental Installation

A schematic diagram of the general layout installed at Warren Station is given in Figure 1. Section *A* is equipped with trifurcating heads at the pipe ends, and with $3^3/_8$-inch outside diameter copper riser pipes to the terminals. Sections *B* and *C* are connected by means of a joint in manhole *12519* and their extreme ends terminate in risers similar to those of section *A*. Between the adjacent terminals of section *A* and section *B* are located the transformers for supplying the cable loads. At the extreme terminals of section *A* and section *C* the free ends of the cable were connected by means of suitable switching equipment to an existing 120-kv overhead line, for the completion of the loop circuit. For this arrangement, the supply transformers were designed for the loading of the cables under test so that a conductor temperature of 70 degrees centigrade could be obtained. In October, 1945, when it became desirable to reach higher conductor temperatures, it was necessary to discontinue this loop arrangement by shorting the terminals at the extreme ends of the cable installation and applying single-phase 120-kv potential, because the 3-phase overhead return circuit of 250,000-cir-mil conductors caused too much loss in the transformers.

While cable sections *A* and *B* are connected in series electrically, they may be made entirely independent of each other as far as maintenance of gas pressure is concerned. Each of the two pipe systems is provided with nitrogen feeding equipment consisting of a standard cylinder of dry nitrogen and a gas-pressure regulator with pressure gauges; in addition, each is equipped with a low gas-pressure alarm set for a pressure of 200 pounds per square inch.

An important question in the operation

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W. D. Sanderson and Joseph Sticher are both with the Detroit Edison Company, Detroit, Mich., and M. H. McGrath is with the General Cable Corporation, New York, N. Y.

The authors are indebted to many of their engineering associates who have so generously contributed to this work and wish to specifically thank M. O. Boyd, A. E. Featham, R. H. Hiester, F. M. Hull, L. I. Komives, H. D. Lewis, G. B. McCabe, R. J. McCausey, J. B. Mulavey, and W. A. Sinclair for their helpful co-operation.

of pipe-type cables is the protection of the pipe against corrosion. As a result of a comprehensive survey, the exterior of the steel pipes is protected along the length of the cable run with four different types of corrosion-preventive coverings that were selected as having sufficient merit for trial. A description of these coverings is given in Appendix I.

It was appreciated in the original planning that it would be desirable to have the sections of pipe that were to have different coatings isolated from each other, electrically, but this was found not to be practical. However, at the end of the test it is expected that the variously coated pipes will be cut apart and their respective insulation resistances measured.

Cables, Joints, Terminals and Accessories

The types of cable that are now or have been under test in this installation include the armored type of compression cable in which the insulated conductors are individually leaded, then cabled, and the whole provided with an armor; the unarmored type of compression cable in which the armoring is omitted; and various *SMD*-type cables, some preimpregnated and others mass-impregnated. A description of the various types of cable used in the successive tests is given in Appendix II. Construction of the joints, terminals, and other accessories such as compensators for the gas-compression type of cable was described in papers[4,4] previously referred to.

Provisions For Artificial Loading

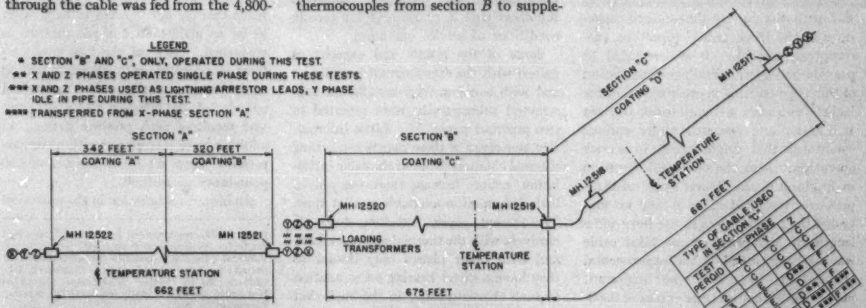
The equipment for circulating current through the cable was fed from the 4,800-volt station house service feed. The rotor of a 4,800-volt 300-kva, 10 per cent, 3-phase regulator was supplied 4,800 volts, and its stator was supplied at 240 volts by a bank of three 100-kva 4,800- to 240-volt transformers. This arrangement provided 0 to 480 volts on the regulated side of the regulator. This variable voltage was connected to a bank of three 100-kva loading transformers, 500-volt to 200-volt ratio, having the 200-volt winding insulated with 139-kv class insulation and in series with the cable.

Provisions For Obtaining Thermal Characteristics

To procure the desired data of the thermal characteristics of the experimental installation, thermocouples were originally provided at 58 locations. One was placed on the shielding tape of one conductor of the cable in section *A*, in the vertical riser at the station end about one foot below the lower end of one terminal, in order to check for excessive cable temperatures in the vertical risers. In order to determine whether excessive temperatures develop in a joint, three other thermocouples were placed on the shielding tape of one conductor of the 3-conductor joint between sections *B* and *C*, one at the center, another at the end of the joint, and the third six inches from the joint wipe on the cable. The remaining 54 thermocouples were grouped in three locations along the length of the cable run in order to enable determination of the temperature distribution from the cable to the surrounding earth up to 20 feet distant from the cable in a plane perpendicular to the cable run. A relocation of thermocouples from section *B* to supplement those at the temperature station on section *A*, in January, 1943, resulted in the present layout as shown in Figure 2. Thirty thermocouples are installed at this location.

In each of the three locations along the cable run in which the temperature distribution from cable to earth is to be determined, a potentiometer type temperature recorder was installed that provides consecutive printing on a strip chart of temperatures from sixteen different thermocouples. With time intervals between consecutive printings of about one minute, the temperature of each thermocouple connected is printed approximately every fifteen minutes. The temperature range is from −20 to 80 degrees centigrade, and the potentiometer is automatically standardized. Housings were erected in the three locations to accommodate the temperature recorders. These houses were thermally well insulated, and provided with electric heaters and ventilators, so that the temperature inside could be maintained essentially constant throughout the year for the proper functioning of the recorders. In early 1943 the temperature station on section *B* was discontinued except for thermocouples on cables and pipe.

The wire used for the thermocouples was copper and constantan of 1938 calibration. The measuring junctions were of number 30 (American wire gauge) wire insulated with enamel and a single layer of silk, and in no case exceeded a length of $3^1/_2$ feet. These wires were connected to copper constantan leads of number 14 (American wire gauge) of sufficient length



TEST PERIOD	DATE START	DATE FINISH
1	3-29-41	12-29-43
2	2-3-44	6-19-44
3	1-29-45	11-6-45
4	11-21-45	3-21-46
5	6-28-46	7-6-46
6	7-22-46	8-26-46
7	8-26-46	9-15-46
8	9-6-47	10-21-47

TYPE OF CABLE USED IN SECTION "A"			
TEST PERIOD	PHASE X	PHASE Y	PHASE Z
1	A	A	A
2	A*	A*	A*
3	A	A	A
4	A	A***	A
5	A	A	A
6	A	A**	A
7	A	A	A
8	H	G	I

TYPE OF CABLE USED IN SECTION "B"			
TEST PERIOD	PHASE X	PHASE Y	PHASE Z
1	B	B	B
2	B	B	B
3	B	B	B
4	B	B***	B
5	B	B	B
6	B	B**	B
7	B***	B***	B***
8	J***	—	A****

Figure 1. Schematic diagram showing installation of experimental line

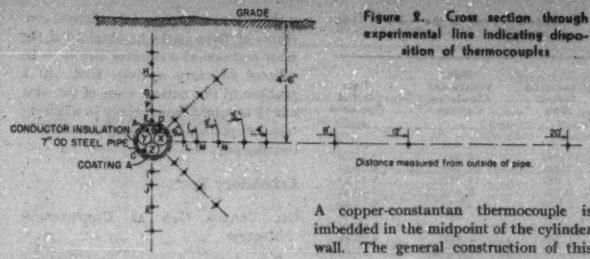


Figure 2. Cross section through experimental line indicating disposition of thermocouples

to reach the temperature stations, the leads of each thermocouple being installed in conduit consisting of 1/2-inch copper tubing buried in the ground. The conduit was used in order to prevent moisture from penetrating the insulation of the thermocouple wires with consequent inaccuracies in the temperature measurements; the high thermal conductivity of the copper conduits would, however, also affect the temperature of the measuring junction, unless proper precautions were taken. The conduit therefore was laid along isothermals for at least nine feet wherever it was considered necessary. In Figure 3 are shown the types of thermocouple installations employed in the three locations where temperature distribution from cable to ground was to be determined. Part (A) of that figure shows the type of thermocouple used for measuring the temperature at the outer surface of the cable including means for bringing the lead wires out through the wall of the steel pipes; part (B) shows the type used for burying in the pipe trench in the vertical line above and below the pipe; and part (C) shows the type used for placing in undisturbed earth in the horizontal line at right angles to the pipe axis.

In order to secure data concerning ambient earth temperatures adjacent to section *A*, an installation was made in late 1941, consisting of eight thermocouples to a depth of seven feet. The type of construction is as shown in Figure 3(B).

Inasmuch as the current-carrying capacity of buried, pipe-type cable is greatly affected by the thermal resistivity of the soil, and little information was available in this respect, provisions were made for determining the thermal resistivity of the soil for different types of soil under varying seasonal conditions. For this purpose, a device is used which consists of a copper cylinder, 12 inches long, 3 inches in outside diameter, with 1/8-inch wall, provided with an internal electrical heating element capable of dissipating 200 watts uniformly over the outside surface. A copper-constantan thermocouple is imbedded in the midpoint of the cylinder wall. The general construction of this device, known as a soil resistivity cell, is shown in Figure 4, and further details for the construction and use of this cell are given in the literature.[5] This cell was buried vertically in the ground, with the heater supply and thermocouple leads brought out to a convenient location. Means were also provided for measuring the ambient earth temperatures.

Provisions For Obtaining Power Factor Data

Provisions were made for obtaining power factor voltage and power factor temperature characteristics of the cables used in the experimental field installation at various voltages in the range from 20 kv to 100 kv between conductor and ground. For this purpose, a Schering Bridge, properly modified to take care of the large charging currents encountered with these long samples of high-voltage cable, was provided. Over-voltage safety devices were employed to protect operator and equipment in case of cable failure or breakdown of the standard capacitor during measurements. The standard capacitor was of the gas-pressure type, operating at a nitrogen pressure of 250 pounds per square inch and usable for voltages up to 150 kv. Precautions were taken in the construction of the outdoor test bus connecting the cable terminals with the measuring setups, so as to minimize inaccuracies in the power factor measurements resulting from changing weather conditions and field distributions in the space of the outdoor station structures through which the test bus was installed.

Loading Schedules

With regard to the loading of the cable in the experimental installation, two distinct purposes were to be served with the original cables placed under test. First, the length of the loading periods was to be sufficient to produce essentially constant temperatures in order to enable the study of the thermal characteristics of the cable installation; second, the number of load cycles was to be as great as possible in order to obtain a maximum amount of mechanical working of the experimental installation in a given time. This second purpose was achieved by subjecting the cable to three or more load cycles a day,

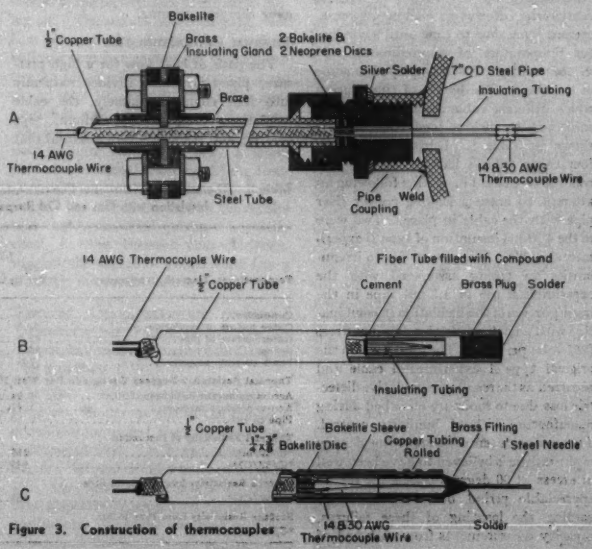


Figure 3. Construction of thermocouples

Table I. Hours of Operation Up to and Including October 21, 1947 of the Various Types of Cable Used in the Experimental Line

Cable Designation	Phase	Date Installed	High Voltage Only	Circulating Current Only	High Voltage and Circulating Current	Date Out of Operation
Type *A*	X	3-29-41	3,077	2,287	11,398	**
	Y	3-29-41	1,296	1,980	8,891	9-15-46
	Z	3-29-41	1,296	2,287	11,398	9-15-46
Type *B*	X	3-29-41	1,718	3,022	13,375	9-15-45
	Y	3-29-41	1,296	2,724	10,868	9-15-45
	Z	3-29-41	1,718	3,022	13,375	9-15-45
Type *C*	X	3-29-41	1,234	1,346	6,351	6-19-44
	Y	3-29-41	1,234	1,340	6,351	6-19-44
	Z	3-29-41	1,234	1,346	6,351	6-19-44
Type *D*	Y	1-29-45	62	1,378	4,517	7- 6-46
Type *E*	X	1-29-45	484	1,676	7,024	**
Type *F*	Z	1-29-45	484	1,676	7,024	**
Type G*	Y	8- 6-47		21	1,781	**
Type *H*	X	8- 6-47		21	1,781	**
Type *I*	Z	8- 6-47		21	1,781	**
Type *J*	X	8- 6-47	1,781			**

* Type of cable used on commercial line.

** Cable still in operation.

each cycle consisting of a loading period of several hours followed by a cooling period of equal duration. The effect of approximately 300 load cycles of short duration on the mechanical movement of cable in the experimental installation has been discussed in other papers.[3,4] After completion of the loading schedule for satisfying the second purpose, all further loading was on the basis of loads of long duration for the purpose of obtaining the thermal characteristics of the cable installation.

Originally, provisions were made to insure that the maximum conductor temperature did not exceed 70 degrees centigrade. However, loadings were increased from time to time until a conductor temperature of approximately 100 degrees centigrade was employed in some of the tests. The length of time during which different cable samples have been subjected to various load conditions is shown in Table I. During these tests, four cable failures have occurred. One was due to charred insulation following an attempt to braze a leaking copper riser pipe with the cable in place. Two were in the 400-mil insulation of type *D* experimental cable. These were due to irregularities in taping associated with the experimental use of 1.5-mil tape in the inner portion of the insulation in combination with a relatively high maximum stress (264 volts per mil). The fourth was in the original type *A* experimental cable and occurred as the result of cumulative dielectric loss due to moisture absorbed during manufacture without humidity control. The conductor temperature in the vicinity of the failure is believed to have been well in excess of 90 degrees centigrade for an appreciable period before failure. Regarding the locating of these failures, capacity measurements from each end of the line locate a fault approximately. Burning current is then applied to reduce the resistance of the fault. Finally, Kenotron current is used to produce a discharge across the fault at about 5-second intervals, and the resulting "bump" facilitates quite exact location of the fault through the use of an electronic stethoscope. For repair of the fault, a segment of pipe 6 inches wide and 7 feet long is removed with a Skilsaw. After repair, a light foil liner (slightly larger than the slot in the pipe) is inserted over the cable as a heat radiation shield. A curved steel plate is then welded in place over the pipe opening.

Thermal Characteristics

The path of heat flow for a high-pressure, pipe-type cable divides naturally into four elements, namely, the cable insulation, compression medium, pipe coating, and the soil. It is essential that a thorough knowledge of the thermal constants of these paths be obtained if the most economical conductor size is to be selected to carry a given load. In a problem of this nature, some of the data can be obtained more readily in a laboratory setup than in an experimental field installation.

Laboratory Tests

Oil Versus Gas as Compression Medium

One of the laboratory investigations was concerned with the heat flow through the compression medium, first, when nitrogen gas is used as the compression medium, and second, when the nitrogen is replaced with oil having a viscosity of 700 Saybolt Universal seconds at 100 degrees Fahrenheit. In this investigation the cable setup was constructed from solid-type single-conductor 750,000-circular-mil paper and lead cable, with 16/32-inch insulation thickness, from which the lead sheath had been removed. Two layers of soft copper tape, 3/4-inch wide by 0.005-inch thick were applied over the insulation, butt wound with a 50-per cent overlay. Over the copper tapes, a $2^1/_2$-inch wide canvas tape, impregnated with paraffin, was applied half-lap wound; and over the canvas, number 8 (American wire gauge) bare, soft-copper wire was wound in a 2-inch open spiral.

Three pieces of the cable were assembled to form a 3-conductor cable; two conductors of this 3-conductor cable were connected at each end in such a manner that the three conductors were connected in series. The conductor forming the center of this series arrangement was 13 feet long, the other two conductors 16 feet each. The connections of the

Table II. Thermal Characteristics of Experimental Steel Pipe Type Cable Using Solid-type Cable Insulation with Gas and Oil Respectively for the Pressure Medium

	Pipe of Cable Test Setup Filled With			
	Nitrogen at		Oil at	
Temperatures, Degrees Centigrade	200 Lb Per Sq In.	100 Lb Per Sq In.	200 Lb Per Sq In.	100 Lb Per Sq In.
Conductor	70.1	70.0	69.2	69.2
Outer surface of insulation	53.8	54.0	53.5	53.3
Outer surface of pipe	42.0	42.0	44.6	44.4
Room	29.3	29.5	32.2	33.0
Load, current, amperes	705	675	720	720
Thermal Resistance Degrees Centigrade Per Watt Per Foot				
Across conductor insulation	1.94	2.08	1.79	1.82
Across pressure medium	1.41	1.56	1.02	1.02
Pipe to air	0.50	0.54	0.47	0.43
Thermal Resistivity of Insulation				
°C/W/in²	216	231	199	202
°C/W/CM³	549	586	505	515
Surface Resistivity from Cable to Pipe				
°C/W/CM³	553	613	401	401
Surface Resistivity from Pipe to Air				
°C/W/CM³	810	870	759	699

Table III. Thermal Characteristics of Pipe-type Cable as Affected by Position of the Cables in the Pipe

Insulation Thickness, Mils	Current, Amperes	Watts Dissipated, Watts Per Ft	Temperature, Degrees Centigrade				Thermal Resistance, °C/W/Ft/Conductor			Thermal Resistivity of Insulation		Surface Resistivity, °C/W/CM²	
			Conductor	Shielding Tape	Pipe	Air	Across Conductor Insulation	Across Pressure Medium	Pipe to Air	°C/W/in¹	°C/W/CM¹	Cable to Pipe	Pipe to Air
200 Lb Per Sq In. Pipe in Original Position													
400	544	9.45	75.3	54.8			2.18	1.10		214	545	285	
450	544	9.65	81.2	58.9	44.3	29.0	2.31	1.51	1.59	210	534	419	859
500	544	9.56	78.6*	58.4			2.50**	1.46					
200 Lb Per Sq In. Pipe Rotated Through 120 Degrees													
400	545	9.67	81.4	60.2			2.19	1.95		214	545	505	
450	545	9.65	80.8	58.0	41.3	28.3	2.36	1.73	1.38	214	545	479	724
500	545	9.58	77.8*	57.1			2.54**	1.65					
200 Lb Per Sq In. Pipe Rotated Through 240 Degrees													
400	545	9.55	77.9	57.0			2.19	1.42		214	545	368	
450	545	9.60	79.3	55.8	43.4	29.4	2.45	1.29	1.44	223	566	357	780
500	545	9.74	83.4*	60.5			2.66**	1.75					
200 Lb Per Sq In. Pipe in Original Position													
400	530	9.05	75.6	55.2			2.26	1.28		222	564	332	
450	530	9.21	81.5	59.2	43.7	31.9	2.42	1.69	1.29	220	559	467	690
500	530	9.28	82.4***	58.6			2.62**	1.61					

* These values are considered to be in error due to the fact that the thermocouple imbedded in the conductor was not securely held in the well drilled for it in the conductor.

** These values were calculated from the data obtained on the conductor with the 450 mils of insulation.

*** Calculated from the shielding tape temperature by means of formula given in text.

conductors were made by pencilling the insulation, 45-degree angle, soldering the conductors together, and insulating the joints with several layers of varnished cambric tape. The test sample was then provided with six copper-constantan thermocouples, placed in a plane perpendicular to the cable axis and located at the center of the cable length. Three of the six thermocouples were installed in the three conductors, and the remaining three were tucked under the layers of soft-copper tape provided over the insulation. This simulated 3-conductor cable was then installed in a 6-inch steel pipe 15 feet long, the ends of which were provided with pressure-tight terminals. Three thermocouples were installed on the outside of the steel pipe in the same plane in which the thermocouples in the cable were located. One thermocouple was placed at the bottom of the pipe on one side, and one on top. The setup was then installed in free air.

The pipe was first filled with dry nitrogen, and tests were conducted at 200 and at 100 pounds per square inch. The nitrogen was then replaced with 700-second oil, and thermal characteristics were determined at 200 and 100 pounds per square inch. Direct current of sufficient magnitude to obtain a conductor temperature of approximately 70 degrees centigrade was provided by two welding generators. In every case, loading was continued until temperature equilibrium of the system had been obtained for four hours.

A summary of pertinent data, together with calculated thermal resistances and resistivities for these conditions is given in Table II. In addition to the data concerning the path of heat flow through the compression medium for which this phase of the study was mainly undertaken, other data that were readily obtained during the test are also included in this table. This additional information is in connection with the heat flow through the insulation of the solid-type cable used and the heat flow from the pipe surface to the surrounding air. The results concerning the heat flow through the compression medium indicate that better thermal characteristics are obtained when the nitrogen is replaced with 700-second oil. In this connection it must be kept in mind, however, that the thermal resistance of the compression medium constitutes only a small part of the thermal resistance of the complete path of heat flow of the cable installation. It is also indicated by the results that the degree of oil pressure has little effect on the thermal characteristics, whereas the degree of gas pressure has a definitely measurable effect.

Thermal Characteristics of Path Through the Pressure Medium as Affected by Position of the Cables in the Pipe

In this laboratory investigation the setup was similar to that of the previous investigation, except that the cable was constructed from *SMD*-type, single-conductor, 400,000-circular mil cable with 400-, 450-, and 500-mil insulation thickness, respectively, employed for the three conductors. The pipe was filled with dry nitrogen at 200 pounds per square inch, and a conductor temperature of approximately 80 degrees centigrade was used. Measurements were made, first with the pipe in the starting position, then with the pipe rotated 120 degrees, and finally with the pipe rotated another 120 degrees in the same direction in order to change the position of the cables in the pipe. A summary of pertinent data resulting from this investigation is given in Table III. Here again, other data are included, such as those covering the heat flow through the cable insulation and the heat flow from the pipe surface to the surrounding air.

The values measured for the conductor temperature of the 500-mil insulation sample appeared to be out of order from the beginning of the test, inasmuch as these temperature values should have been higher than those for the samples provided with the 400-mil and 450-mil insulation. When the test setup was disassembled it was found that the thermocouple imbedded in the conductor of the 500-mil insulation was loose, that is, not securely held in the well drilled for it in the conductor. Also, near the end of the test the thermocouple circuit opened inside the pipe and, consequently, further measurements could not be obtained.

The conductor temperatures measured for the 500-mil insulation sample cannot, therefore, be regarded as valid, and calculated thermal resistances for this sample based on the measured conductor temperature must also be considered to be in error. Inasmuch as the thermal resistances given for the 400-mil and 450-mil insulation samples are probably correct

Table IV. Effect of Gas Pressure and of Temperature on the Thermal Characteristics of *SMD* Type Cable

Insulation Thickness, Mils	Current, Amperes	Watts Dissipated, Watts Per Ft	Temperature, Degrees Centigrade: Conductor	Shielding Tape	Pipe	Air	Thermal Resistance, °C/W/Ft/Conductor: Across Conductor Insulation	Across Pressure Medium	Pipe to Air	Thermal Resistivity of Insulation: °C/W/in²	°C/W/CM²	Surface Resistivity, °C/W/CM²: Cable to Pipe	Pipe to Air
200 Lb Per Sq In.													
400	544	9.45	75.3	54.7			2.18	1.10		214	545	285	
450	544	9.65	81.2	58.9	44.3	29.0	2.31	1.51	1.59	210	534	419	859
500	544	9.58	78.5*	58.3			2.50**	1.46					
100 Lb Per Sq In.													
400	530	9.00	76.0	56.1			2.21	1.42		216	550	368	
450	530	9.25	83.0	60.9	43.3	29.1	2.39	1.92	1.56	218	554	530	836
500	530	9.09	79.4*	59.7			2.60**	1.81					
0 Lb Per Sq In.													
400	490	7.70	76.0	58.5			2.28	2.12		224	569	550	
450	490	7.92	85.5	65.2	42.2	30.7	2.56	2.91	1.47	232	590	805	793
500	490	7.79	80.1*	63.0			2.76**	2.67					
200 Lb Per Sq In.													
400	530	9.05	75.6	55.2			2.26	1.28		222	564	332	
450	530	9.21	81.5	59.2	43.7	31.9	2.42	1.69	1.29	220	559	467	690
500	530	9.28	82.4***	58.6			2.62**	1.61					
200 Lb Per Sq In.													
400	290	2.44	44.6	38.6			2.44	1.36		239	607	353	
450	290	2.46	46.5	40.1	35.3	31.5	2.60	1.94	1.56	236	600	536	837
500	290	2.46	46.4***	39.9			2.81**						

* These values are to be considered in error due to the fact that the thermocouple imbedded in the conductor was not securely held in the well drilled for it in the conductor.

** These values were calculated from the data obtained on the conductor with the 450 mils of insulation.

*** Calculated from the shielding tape temperature by means of formula given in text.

and their ratio is that which one would expect from the ratio of the geometric factors of these two samples, the thermal resistance of the 500-mil insulation sample can be calculated by using the experimentally determined thermal resistances for the 450-mil insulation sample and the corresponding geometric factor with that of the 500-mil insulation sample. This was done and the values so calculated are shown in Table III. These same comments hold true in connection with the results given in Table IV.

It is interesting to note that, while the position of the cables in the pipe has no effect on the thermal resistivity of the cable insulation, which is obviously correct, and no effect on the surface resistivity from pipe to the surrounding air, the effect on the thermal resistivity for the part from the cable to the pipe is quite pronounced.

Effect of Gas Pressure and of Temperature on the Thermal Characteristics of *SMD*-Type Cable

In the experimental field installation, conductor temperatures under certain conditions were determined by means of resistance measurements of the conductors. This applied mainly to cables in section *A*, and to cables in sections *B* and *C* only when the cables in these sections, which are permanently connected to each other by means of a manhole joint, were not of a dissimilar conductor size. There were occasions, however, in which cables of dissimilar conductor size were connected in series in these sections, and this made it impractical to apply the method of conductor resistance measurements for determination of conductor temperatures. For use in such cases a formula was developed which allowed calculation of the conductor temperature from data normally obtained in connection with the field tests, if the thermal resistance values of the conductor insulation are known. This formula is as follows:

$$T_c = \frac{T_s + I^2 R_{th} R_r (1 - T_r \alpha_r)}{1 - I^2 R_{th} R_r \alpha_r},$$

where

α_r = temperature coefficient of conductor resistance at reference temperature T_r

I = conductor current, amperes

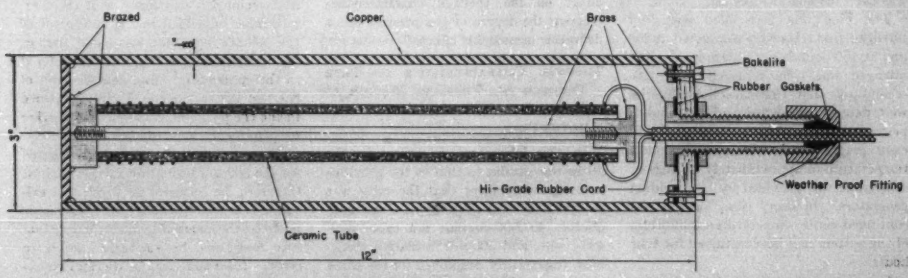
R_r = conductor resistance at reference temperature T_r, ohm per foot

R_{th} = thermal resistance from conductor to shielding tape, degrees centigrade per watt per foot

T_c = conductor temperature, degrees centigrade

T_r = reference temperature for which conductor resistance is known, degrees centigrade

T_s = shielding tape temperature, degrees centigrade



Note: Ceramic Tube Wound With Chromel Wire To Dissipate 200 Watts At 120 Volts

Figure 4. Soil resistivity cell

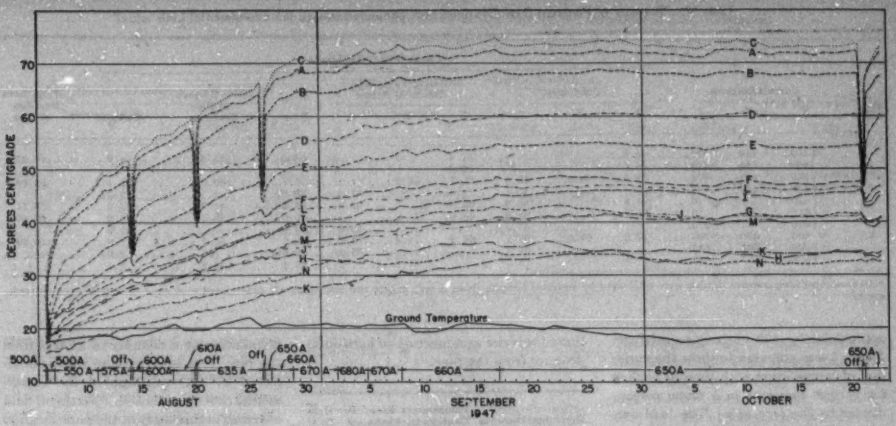


Figure 5. Time-temperature curves for component parts of the thermal circuit

Cables type *G*, *H*, and *I*

This formula is strictly applicable only when the dielectric loss and other losses are negligible as compared to copper loss.

One case in which test cables of dissimilar conductor size were connected in series involved 400,000-circular mil, *SMD*-type cable that was connected to 650,000-circular mil cable of the compression type. The 400,000-circular mil, *SMD* cable was provided with an insulation thickness of 400 mils for one conductor, 450 mils for another, and 500 mils for the third. It was intended to load the cables in such a manner that conductor temperatures of the order of 100 degrees centigrade would be reached in the 400,000-circular mil conductor. For this purpose it was necessary to know the thermal resistance value of the conductor insulation, and particularly whether this value changes greatly with variations in gas pressure and in temperature.

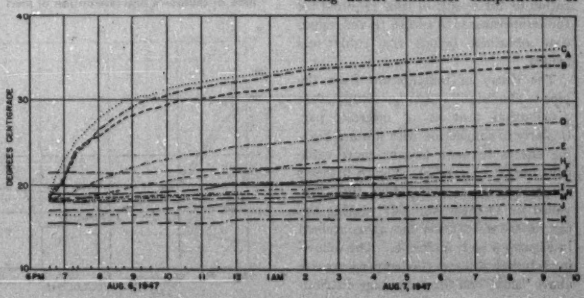
The same test setup that was described for the study of the effect of position of the cables in the pipe on the thermal characteristics was used in this investigation. The pipe was filled with dry nitrogen, and tests were conducted at 200 and 100 pounds per square inch, and atmospheric pressure, with the conductor temperature at approximately 80 degrees centigrade. In addition, one test was conducted with a gas pressure of 200 pounds per square inch and a conductor temperature of approximately 45 degrees centigrade. In every case, loading was continued until temperature equilibrium had obtained for four hours. Pertinent results in this investigation are given in Table IV. Here again, other data that were readily obtained during the test are also included in addition to the data concerning the path of heat flow through the conductor insulation for which this phase of the study was mainly undertaken. Some of these additional data are discussed under "Field Tests–Surface Resistivity from Cable to Pipe," together with data obtained in the field.

Concerning the effect of pressure on the thermal resistance of the conductor insulation it appears that any change between 200 and 100 pounds per square inch is so small that it would be masked by the experimental uncertainty of the method. At atmospheric pressure, a small increase of thermal resistance over that at 200 pounds and that at 100 pounds per square inch is indicated. It will be realized, however, that the thermal resistance at atmospheric pressure is essentially of academic interest only, inasmuch as the cable will not be operated at that pressure.

Regarding the effect of temperature on the thermal resistance of the conductor insulation, it appears that in the range of conductor temperatures from 80 to 45 degrees centigrade, the increase in thermal resistance with decrease in temperature is in the neighborhood of 10 per cent.

Figure 6. Temperature rise during first 15 hours

Cables type *G*, *H*, and *I*



Field Tests

Temperature Surveys

From the tremendous amount of temperature data that have been procured from the field installation, some specific data have been selected in order to show the relative temperatures that obtain in various parts of the field installation under certain operating conditions.

In one test run the cable was subjected to 3-phase loading that was calculated to bring about conductor temperatures of

Table V. Summary of Pertinent Data Obtained from Measurements on the Experimental Line

Test	Current Amperes X	Current Amperes Y	Current Amperes Z	Temperature, Centigrade: Conductor X	Conductor Y	Conductor Z	Shielding Tape Lead Sheath of Armor X	Shielding Tape Lead Sheath of Armor Y	Shielding Tape Lead Sheath of Armor Z	Pipe	Outer Surface of Pipe Covering	Ambient	Watts Loss Per Foo of Pipe
1	469	449	472	43.6			31.4			28.0	25.5	11.9	16.95
	469	449	472	38.6			26.7						
	469	449	472	58.6	54.4	59.8	41.7	40.2	41.2	36.7	30.5	12.1	26.05
	469	449	472			45.5			37.5	30.0		12.3	
2	610		610	101.8		100.8	69.0		65.3	48.8	41.7	18.9	33.74
	610		610	66.2		66.2	44.0			40.0	36.1	18.6	20.40
	610		610			67.5			53.0	42.9		19.0	
3	605	650	705	87.6	99.9	97.2	68.0	71.8	73.3	60.0	54.2	17.8	40.29
4	608	607	620			67.0			52.2	39.8		5.0	
	608	607	620			73.5*		51.2		38.3	29.8	5.1	

* All conductor temperatures in table were calculated by means of formula given in text, except this one which was determined from conductor resistance measurements.

approximately 100 degrees centigrade. Figure 5 was prepared to show the variation of temperature with time of loading for various locations in a plane perpendicular to the pipe axis. The load currents given in this figure refer to the currents in the Y-phase which contained cable of type *G*. The currents in the X-phase (type *H* cable) and the Z-phase (type *I* cable) were approximately 600 and 700 amperes, respectively, when the current in the Y-phase was 650 amperes. This unbalanced load could not be readily alleviated, and this unbalance must be kept in mind when considering such temperatures as those of the shielding tapes. Figure 5 affords an interesting insight into the time lag of temperature at various radial distances from the cable conductors. Figure 6 is included to show in greater detail the temperature changes that occur in the various locations during the first 15 hours following application of the load; and, Figure 7 gives a more detailed picture of the temperature decay resulting when the load is discontinued and the temperature increase that occurs when the load is reapplied. Figure 8 was constructed from data obtained from another test run that was terminated when type *A* cable failed on September 15, 1946. This figure gives an excellent indication of the length of time required for the temperatures to return to the values that obtained before the cable was loaded.

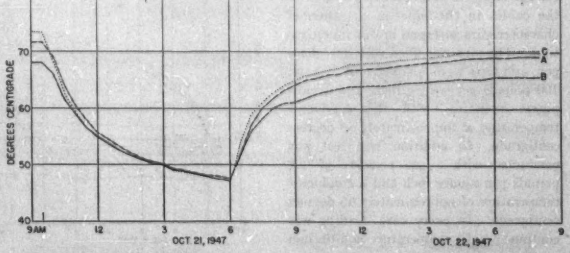
The decay of temperature from the pipe to the "sink" is another temperature distribution that is of interest, particularly in connection with mutual heating effects between two or more cable runs buried in parallel. Figure 9 shows the temperature distribution that prevailed on October 15, 1947, during the test run for which other data are contained in Figures 5 to 7, inclusive. The following tabulation shows the temperature rise above "sink" and the percentage distribution of this rise as a function of horizontal distance from the pipe.

Horizontal Distance from Pipe	Temperature Rise, Centigrade, Above Ambient	Per Cent of Total Rise
7/16 inch (outer surface of pipe covering)	36.4	100
6 inches	28.6	78.5
1 foot	23.5	61.8
2 feet	14.7	40.4
4 feet	9.6	23.6
8 feet	2.7	7.4
12 feet	1.0	2.7
20 feet	0	0

The foregoing data show that approximately fifty per cent of the temperature rise in the earth occurred under these conditions within a distance of one and a half feet from the pipe.

Another phase of the temperature survey, one that has been continued for a number of years, is concerned with ground temperatures that prevail at Detroit during the different seasons of the year at various levels below the ground surface. These temperatures were determined at the location of the experimental installation at a distance of 21 feet from the pipe, that is, in ground that is unaffected by the operation of the experimental cable installation. Pertinent data obtained in this study at the same location and at the same time of the year were averaged for three successive years, and were used in the curves of Figure 10.

Figure 7. Time-temperature characteristics of shielding tapes of cables type G, H, and I, at time of shutdown and resumption of load



Soil Resistivity

Data concerning resistivity values of several types of soil have been collected during the past several years. Cylindrical cells of the type shown in Figure 4, equipped with voltage regulating devices, have been installed. Close regulation of the supply circuit, as well as the use of the maximum permissible current, considerably reduced the percentage of error and fluctuating readings. The data obtained with these cells were introduced into the following formula in order to obtain the thermal resistivity of the soil:

$$\rho = \frac{128}{W}(T_1 - T_2)$$

where

ρ = thermal resistivity of soil, degrees centigrade per watt per centimeter cube

W = watts dissipation, watts

T_1 = temperature of cylinder wall, degree centigrade

T_2 = temperature of ambient earth, degree centigrade

This formula is empirical and was

Table V. Summary of Pertinent Data Obtained from Measurements on the Experimental Line (Continued)

Test	Thermal Resistance °C/W/Ft/Cond: Pressure Medium X	Y	Z	Pipe Covering	Earth	Thermal Surface Resistivity from the Cable Surface to the Inner Wall of the Pipe (°C/W/CM²): X	Y	Z	Thermal Resistivity °C/W/CM²: Pipe Covering	Earth	Type of Cable	Pipe Covering	Line Section in Which Measurements Were Made
1	0.71			0.44	2.41	200			241.5	46.5	A	A	A
											A	3¼-inch copper riser pipe at terminal of A	
	0.66	0.51	0.58	0.71	2.12	183	132	171	76.0	53.8	D, E, F	D	C
											B	C	B
2	1.35	1.11		0.42	1.35	350		308	67.0	51.6	E, F	D	C
	0.44			0.38	1.71	162			311.5	49.7	A	A	A
											B	C	B
3	0.83	1.03	1.00	0.43	2.71	304	376	830	235.0	52.5	H, G, I	A	A
4											B	C	B
											C	D	C

derived by comparison of the results obtained with such a cylinder and those obtained with a sphere.[5] Test results obtained at each location during several years have been averaged to eliminate radical variations, and pertinent results are shown in Figure 11. In only one case (in yellow clay) has a "run-away" condition occurred in the resistivity value (not shown). In that case a dissipation of 50 watts from the one-foot long, 3-inch diameter cell resulted in the cell temperature increasing from 58.9 to 82.2 degrees centigrade within nine days during which time the ambient temperature was essentially constant. This caused a drying out of a 1/2 inch layer of earth. A curve of resistivity prevailing at one location along the commercial pipe line is also included in Figure 11. This curve was constructed from data obtained by means of measurements made on the commercial line. The thermal resistance calculated from data of temperature drop from pipe covering to ambient and of watts dissipated was introduced into the following formula in order to obtain the thermal resistivity.

$$R_{th} = \frac{\rho}{2\pi} \log_e \left[\frac{4l}{D}\right]$$

where

R_{th} = thermal resistance of soil, in watt–centimeter units

ρ = thermal resistivity of soil, in watt–centimeter units

l = distance below surface of earth of axis of buried pipe, in inches

D = outside diameter of pipe, including protective covering, in inches

The following tabulation shows the relationship between the resistivity as determined from the experimental cable line in a similar manner and a soil resistivity cell which was in operation nearby:

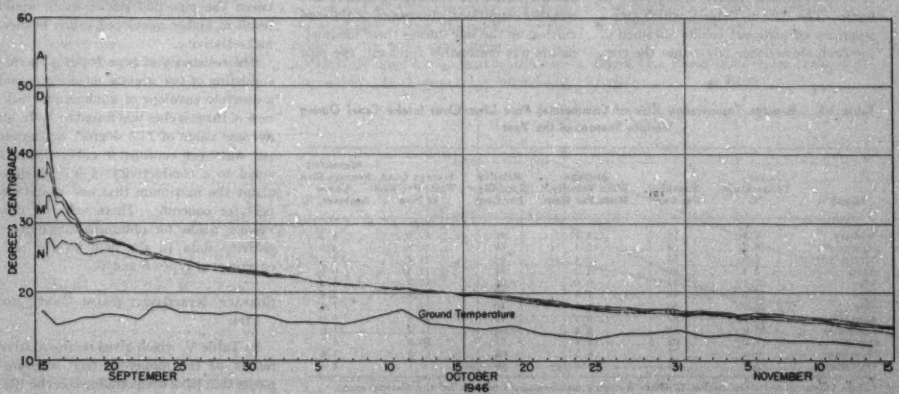
Test	Thermal Resistivity C/W/Cm²: Experimental Line	Soil Cell
1	46.5	61.0
	53.8	61.0
2	51.6	64.0
	49.7	64.0
3	52.5	74.0
Average	50.8	64.8

The foregoing results show the soil cell data to be consistently higher than those determined from the experimental line and therefore such data may be safely used for current rating calculations. In general, the resistivity values have not been found to correlate to precipitation, cloudiness, or transient temperature fluctuations. Lower readings appear prevalent in late winter. In summer, they are somewhat higher. Presumably the pipe and cells are deep enough in the earth so that rainfall is substantially uniformly distributed in time and space before reaching them.

In connection with determination of the soil resistivity, moisture determinations have been made from time to time in various locations. The moisture content of the soil obtaining at the time when data for test 3 in the foregoing table were being procured, and in the same location ranged from 12.8 to 21.1 per cent, with an

Figure 8. Long time cooling curves

Cables type G, H, and I



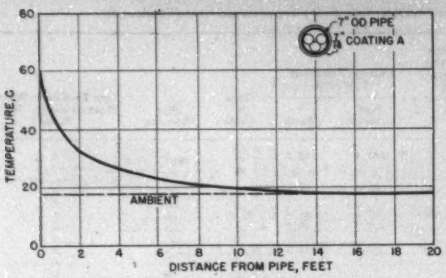


Figure 9. Temperature distribution through earth

Cable section *A*

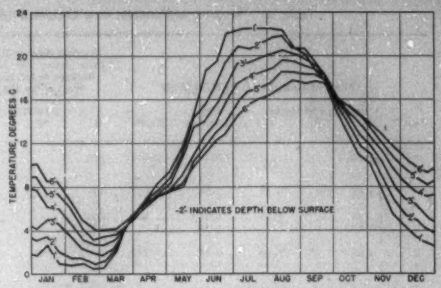


Figure 10. Ambient earth temperatures

average value of 17.3 per cent for nine determinations. An effort was also made to determine the moisture distribution for various distances from the pipe, ranging from 6 inches to 25 feet. Little information has been obtained to date, but the results indicated that the moisture content decreased as the distance increased to $2^1/_2$ feet and remained essentially constant for the rest of the distance investigated. At a distance of six inches the moisture content was 19.9 per cent, at $1^1/_2$ feet it was 18.2 per cent, and from $2^1/_2$ feet to 25 feet it remained essentially constant at 17.1 per cent. The type of soil changed from fine cinders, sand, and tan clay at the six-inch distance to tan and gray clay as the distance was increased.

Surface Resistivity from Pipe to Air

One of the items the cable engineer must consider is the operating temperature of the cable line in air at such locations as terminals, canal crossings, and so forth. The data in Table V which give a summary of pertinent results obtained in the field, show under test 1 that the conductor temperature in the terminal $3^5/_8$-inch riser pipe was five degrees lower than that of the cable operating in the seven-inch pipe buried in the ground and under the same loading conditions.

Tables II and III include data of the surface resistivity values obtained on the pipe used in the laboratory tests. The resistivity was found to vary from 690 to 870 with an average value of 774 degrees centigrade per watt per square centimeter. Insulated Power Cable Engineers Association recommends 1,200 for diameters over 1.75 inches whereas the foregoing results show that for a bare pipe in air (and presumably also for a pipe in air with a protective covering of type *A*) it would be safe to use a value of 850 degrees centigrade per watt per square centimeter provided the pipes were shielded from the sun or were installed indoors.

A portion of the commercial pipe line with type *A* coating is exposed to free air outdoors where it crosses the intake canal for the Delray Power House. Numerous temperature measurements were made at this location and some of the results are shown in Table VI together with related information concerning loading and weather conditions. Inasmuch as the load carried by the line during these measurements was reasonably constant, the pipe temperature changes as shown are a direct function of atmospheric conditions.

Table VI. Average Temperature Rise of Commercial Pipe Line Over Intake Canal During Various Seasons of the Year

Month	Air Temperature, °C	Sunshine, Per Cent*	Average Wind Velocity,* Miles Per Hour	Relative Humidity,* Per Cent	Average Load, Watts Per Foot of Pipe	Measured Average Rise Above Ambient, °C
January	4.3	16	11.1	66	11.0	4.2
February	10.7	28	16.1	69	10.8	1.6
March	2.6	54	12.1	66	11.5	3.4
April	3.4	29	11.3	87	11.4	0.6
May	20.1	81	9.0	65	11.3	5.9
June	22.6	48	7.4	77	11.4	2.9
July	26.2	66	8.8	50	12.7	7.1
August	30.0	92	8.1	40	12.3	9.0
September	23.9	92	6.4	58	11.2	11.0
October	17.2	82	12.7	57	10.9	4.7
November	6.4	7	8.5	69	11.3	3.6
December	−2.7	42	9.6	56	9.1	2.3

* Data taken from United States Weather Bureau's meteorological reports for the Detroit area.

Thermal Resistivity of Pipe Coverings

Regarding the thermal resistivity of type *A* pipe covering composed of asphalt, asbestos, sand and mineral filler, the field tests yielded an average value of 263 degrees centigrade per watt per centimeter cube. This is equal to a conductivity of 2.68 Btu per hour per square foot per degree Fahrenheit per inch of thickness. Results of laboratory tests in which slabs of the material were used gave values of 7.75 to 7.90 with an average of 7.83 for the conductivity. The field tests resulted in higher thermal resistivity values possibly because of imperfections in the bond between the pipe and the covering that might have existed at the location where the measurements were made. Inasmuch as temperature measurements were made at the pipe surface and the outer surface of the covering, an air space between the pipe and the covering would result in higher calculated values of thermal resistivity.

The resistivity of type *D* pipe covering consisting of two acetate membranes and a concrete envelope of a minimum thickness of three inches was found to have an average value of 71.5 degrees centigrade per watt per centimeter cube. This is equal to a conductivity of 9.7 which is about the maximum that one would expect for concrete. There were no provisions made for obtaining thermal resistivity data in the field for the pipe coverings of types *B* and *C*.

Surface Resistivity from Cable to Pipe

In Table V, which gives representative results of the numerous field measurements that have been obtained on the 120-

Table VII. Loss Measurements on Experimental Line to Determine Effect of the Steel Pipe During A-C Operation

Voltage, Volts			Current, Amperes			Watts	Conductor: Measured D-C Resistance, Ohm			Conductor: Temperatures, C		
XY	XZ	YZ	X	Y	Z		X	Y	Z	X	Y	Z
Three-phase operation												
40.3	38.2	47.2	477	455	468	12,000	0.01428	0.01351	0.01357	42.5	38.0	40.5
Single-phase operation												
		61	621		621	14,160	0.0148		0.0148	51.3		51.3

CALCULATIONS

Three-Phase D-c Conductor Loss

$X - 477^2 \times 0.01428 = 3,250$

$Y - 455^2 \times 0.01351 = 2,800$

$Z - 468^2 \times 0.01357 = 2,970$

Total I^2R loss $= 9,020$ watts
Total a-c loss $= 12,000$
Extra a-c loss $= 12,000 - 9,020 = 2,980$

$$\frac{\text{Extra a-c loss}}{\text{D-c Conductor loss}} = \frac{2,980}{9,020} \times 100 = 33 \text{ per cent}$$

Single-Phase D-c Conductor Loss

$X - 621^2 \times 0.0148 = 5,710$

$Z - 621^2 \times 0.0148 = 5,710$

Total I^2R loss $= 11,420$ watts
Total a-c loss $= 14,160$
Extra a-c loss $= 14,160 - 11,420 = 2,740$

$$\frac{\text{Extra a-c loss}}{\text{D-c Conductor loss}} = \frac{2,740}{11,420} \times 100 = 24 \text{ per cent}$$

Note: Data are for cable with 600,000-circular mil, concentric round conductor and 600 mils of insulation lying along the contour of a 7-inch outside diameter steel pipe.

kv experimental line, it is shown that the thermal surface resistivity, from the cable surface to the inner wall of the pipe, for the *SMD* 400,000-circular mil cable varied from 132 to 350 with an average value of 229 degrees centigrade per watt per square centimeter. This is somewhat lower than the laboratory value of 400 which is an average of the values given in Table III. In the case of the *SMD* 600,000-circular mil cable the variation was found to be from 162 to 376 with an average of 286, which is again lower than the 553 value of Table II obtained in the laboratory test using the 750,000-circular mil conductor with 500 mils of insulation.

In computing the values, the surface area was taken to correspond to that of a cylinder having a diameter equal to 2.155 times the diameter over the shielding tape of an individual cable.

In the laboratory tests a six-inch inside diameter pipe was used and the conductors were held in a triangular configuration. On the experimental line a seven-inch outside diameter pipe is used and the cables lie along the contour of the pipe. The fact that position of the cables in the pipe has an appreciable effect on the surface resistivity from cable to pipe has been shown in the laboratory tests already discussed. This effect of position is probably brought about in some measure by differences in the contact between the outer surface of the cable (in some cases copper skid wires) and the inner surface of the steel pipe. Also, the position of the cables in the steel pipe might have an appreciable effect on the heat transfer by convection. In a paper by Whitehead and Hutchings[6] it is pointed out, for instance, that the convection loss for a cable lying on the bottom surface of a duct is 30 per cent lower than that for a cable that is in a concentric position with respect to the duct when the ratio of cable diameter to duct diameter is 0.5. The probability that heat transfer by convection has a considerable bearing on the value of surface resistivity from the outer surface of the cable to the inner surface of the pipe was indicated in the laboratory tests in which the effect of gas pressure was studied. The fact that a definite increase in resistivity with decrease in gas pressure was found cannot be explained by a change in heat transfer by conduction, since the thermal conductivity of a gas does not change for pressures in this region. It is also well known that gas pressure has no measurable effect on the heat transfer by radiation. This leaves us the only possibility changes in the heat transfer by convection. That heat transfer by convection increases with the density (pressure) of a gas is shown, for instance, by Jakob and Hawkins.[7]

The preceding results show that a value of 500 may be safely used for high-pressure gas-filled cable while the laboratory tests suggest 400 for high-pressure oil-filled cable as a fair working basis for current rating calculation.

Figure 11. Composite, thermal resistivity curves of soil

A. Cell adjacent to commercial line
B. Commercial line
C. Cell adjacent to experimental line, section *A*

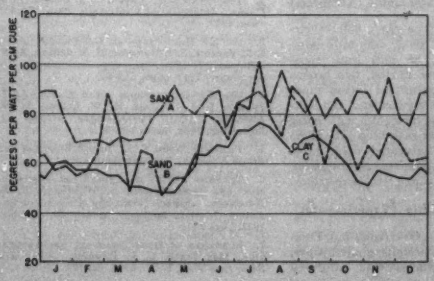
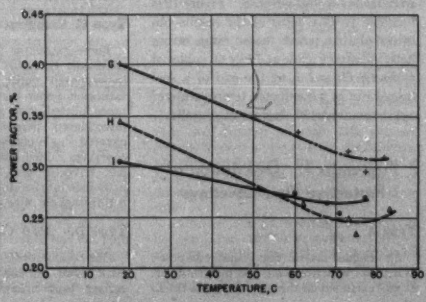


Figure 12. Power factor versus temperature, at 70 kv



Thermal Resistances–Conductor to Sink

Perusal of the data given in Table V for the purpose of determining the percentage contribution of the four elements constituting the total thermal path from each conductor to sink yields the following values:

Thermal Path	Average Thermal Resistance Ohms/Ft/Conductor	Per Cent of Total Resistance
Insulation	2.48	42.7
Gas medium	.78	13.4
Pipe covering	.48	8.3
Earth	2.06	35.6

Increase in Losses Due to the Steel Pipe

One of the problems in determining conductor sizes for new projects or thermal constants on existing pipe-type systems is the matter of a-c/d-c losses. Section *A* of the experimental line provided an excellent opportunity to check these losses for three cables with 600,000-circular mil, concentric round conductor and 600 mils of insulation lying along the contour of a 7-inch outside diameter steel pipe. The results of such measurements are shown in Table VII.

It was found that the ratio of all a-c loss effects to the conductor d-c loss amounted to 33 per cent for 3-phase operation and 24 per cent for single-phase operation for the particular geometric arrangement used in this test.

Power Factor Measurements

Inasmuch as many of the calculations to determine the thermal characteristics were based on conditions during which the cable was subjected to a potential of 120 kv (69.4 kv to ground), it was necessary to know the dielectric loss pertaining to that condition, and a considerable number of power factor measurements were made for that purpose. Figure 12 is included to give some of the results obtained during power factor temperature determinations made at 70 kv on cables of types *G*, *H*, and *I*, at the end of a sustained run at a conductor temperature of approximately 100 degrees centigrade.

Appendix I. Description of Protective Pipe Coverings

Type *A*

An asphalt-mastic compound composed of asphalt, asbestos, sand and mineral filler. It was extruded on the pipe, hot, to a thickness of 7/16 inch and weighs 135 pounds per cubic foot.

Type *B*

A plasticized coal-tar enamel, including two layers tar asbestos felt, with an exterior layer of Kraft paper.

Type *C*

A hot application of an adherent coating of wax, chemically and mechanically fortified, a spiral wrapping of reinforced asbestos, a hot application of a service coat incorporating natural asphalt, a spiral layer of a single membrane wrapper, and a spiral wrapping of heavy Kraft paper.

Type *D*

The pipe was coated, previous to installation, with two acetate membranes, applied with hot wax. Concrete was then poured around the pipe, in the trench, to a minimum thickness of three inches.

Appendix II. Description of Cable Constructions Used in the Experimental Line

Type *A*. *SMD* Cable (Experimental—Manufactured Without Humidity Control)

One-conductor 600,000-circular mil, concentric round, 600 mils preimpregnated paper, bronze tape intercalated with saturated cotton tape, cotton webbing tape lapped. Cable shipped with temporary lead sheath.

Type *B*. Compression Cable (Unarmored)

Three-conductor type *HSL*, 650,000-circular mil, Compack oval, 500 mils mass-impregnated paper, 85 mils lead, three saturated manila tapes butted, two bronze tapes butted, two saturated cotton tapes intercalated, two saturated cotton webbing tapes intercalated. Separately insulated and lead covered single conductor cables with outer protective coverings were cabled without fillers or outer binder. During installation the lead sheath of one conductor was perforated with holes approximately 1/8-inch diameter at 25-foot intervals.

Type *C*. Compression Cable (Armored)

Three-conductor type *HSL*, 650,000-circular mil, Compack oval, 500 mils mass-impregnated paper, 85 mils lead, three saturated paper tapes butted, two bronze tapes butted, two saturated cotton tapes intercalated. Separately insulated and lead-covered single conductor cables with outer coverings cabled with saturated jute fillers and covered with saturated duck tape, 5/64-inch jute bedding and one layer number 8 Birmingham wire gauge steel wire armor.

Type *D*. *SMD* Cable (Experimental)

One-conductor 400,000-circular mil, Compack round, 400 mils preimpregnated paper, copper tape intercalated with saturated cotton tape, cotton webbing tape lapped, two half-round skid wires 200 by 100 mils applied open spiral. Cable shipped with a temporary lead sheath.

Type *E*. *SMD* Cable (Experimental)

Similar to type *D* except with 450 mils preimpregnated paper.

Type *F*. *SMD* Cable (Experimental)

Similar to type *D* except with 500 mils preimpregnated paper.

Type *G*. *SMD* Cable (Commercial, 1941 Warren-Delray Circuit)

One-conductor 600,000-circular mil, concentric round, 600 mils preimpregnated paper, bronze tape intercalated with saturated cotton tape, cotton webbing tape lapped, two half-round copper skid wires 200 by 100 mils applied open spiral. Cable shipped with temporary lead sheath.

Type *H*. *SMD* Cable (Experimental—Mass-impregnated)

One-conductor 600,000-circular mil, Compack round, 600 mils mass-impregnated paper, two copper tapes intercalated, 2 half round copper skid wires 200 by 100 mils applied open spiral. Cable shipped on weathertight reel with dry nitrogen.

Type *I*. *SMD* Cable (Experimental—Mass-impregnated)

One-conductor 600,000-circular mil, Compack round, 500 mils mass-impregnated paper, copper tape intercalated with polyethylene tape, two copper tapes intercalated, two half-round copper skid wires 200 by 100 mils applied open spiral. Cable shipped on weathertight reel with dry nitrogen.

Type *J*. *SMD* Cable (Experimental—Mass-impregnated)

Similar to Type *I* except with 400,000-circular mil, Compack round conductor and 400 mils mass-impregnated paper.

Note: Strand and insulation shielding used on all cables.

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Discussion

R. J. Wiseman (The Okonite Company, Passaic, N. J.): We owe a vote of thanks to the authors for their paper dealing with the thermal characteristics of a pipe system. I look upon the paper as broader in nature than applying particularly to a gas-filled cable installation, as the data presented for the external thermal field is applicable to all kinds of buried cables. Also, they give some thermal constants for the internal field for a gas pressure system which can be compared to oil pressure systems. The transient temperature section is quite helpful in appreciating what overloads can be carried on a buried cable.

The considerable amount of data which is reported in the paper will require a long study to properly appreciate. Table II gives a comparison between the cable assembly using gas and oil as the pressure medium. We would expect the oil to give lower thermal constants. For the insulation, we have been taking 550 thermal ohms per centimeter cube for the gas set-up, and 450 for the oil. Is it possible that because the oil tests were made after the gas tests that the insulation had not reached complete saturation at the time the oil tests were made? For the gas zone and 200 pounds per square inch, we use the formula $R_{th}=2.06N/CD$, where N is the number of conductors, and CD is the circumscribed diameter over the insulated cables assumed to be assembled in a triangular arrangement. The constant 2.06 is equivalent to a surface resistivity of 500 thermal ohms per centimeter square, whereas the authors report a value of 553. I am inclined to believe their value is influenced by the canvas tape wrapped over the copper shielding tape. Taking the foregoing formula, we get a thermal resistance of 1.40 thermal ohms per foot without considering the canvas tape, whereas they get 1.41 thermal ohms per foot. For the oil zone, we assume the cylindrical log law for calculating the thermal resistance, the diameters being the inside diameter of the pipe and the circumscribed circle around the insulated conductors. The thermal resistivity was determined by test and is a function of temperature, as this affects the convection of oil. The thermal resistance computed by us would be about 40 per cent of the value given for the oil tests in Table II. In the case of oil, we have viscosity, convection, and conduction as influencing factors for the thermal resistance. The authors state that they used 700-second oil for the pressure medium. The difference in thermal resistance for our computed value and what they get can be explained on their using a much higher viscosity oil, which greatly affects the apparent thermal resistivity. For example, we use a value of 110 thermal ohms per centimeter cube for a conductor temperature of 70 degrees centigrade, and a value of 180 when the conductor temperature is 45 degrees centigrade. The respective viscosities are in the ratio of four to one. We have found the surface resistivity from pipe to air the same as reported in Table II, namely, about 850 thermal ohms per centimeter cube.

Table III, showing the influence of the position of the cables in the pipe, is a bit puzzling. In the original position, the surface resistivity for cable to pipe shows the lowest value and for pipe to air, the highest. Why should pipe to air be influenced by what is happening inside the pipe? Also, what caused the variation for cable to pipe? The shielding tape temperatures were closely the same for all positions, but the pipe temperature varied. Was there a breeze which might have cooled the pipe for the 120 degree, 240 degree positions, and second original position? Were the cables so placed in the pipe that on rotation they might have become suspended in the pipe?

Figure 9 showing the temperature distribution from the surface of the pipe to a point 20 feet away is very instructive. As the authors state, 50 per cent of the temperature rise occurs within 1.5 feet of the pipe. Some of us wonder why we take the law of images in computing the thermal resistance for a buried cable or pipe and not a cylindrical shaped field having a radius say 10 times the diameter of the cable or pipe. All but a very small percentage of the temperature rise will fall within it, and we can neglect the earth outside it, which rises in temperature very slowly so actually does not greatly influence the total rise.

The need for a better understanding of the shape of the thermal field around a buried cable in pipe and the proper formula to use to calculate the thermal resistance is brought out in the paper, where a comparison of the thermal resistivity of the earth is computed from a soil cell and also the experimental line, using the law of images formula for the latter. I feel that some of the difference is due to the assumption which we all take that the law of images formula applies. Some of us are questioning it. A study of Figure 5 is worthwhile, as it shows how the temperature at points K, H, and N, which are 2 feet from the pipe, are closely the same, indicating a cylindrical field condition, rather than one based on the law of images.

Figure 11 showing the variation in soil thermal resistivity monthly for a year is quite interesting. Although there is an indication of an increase during the summer months, the magnitude is not very great, and it is safe to take an average value independent of time of year without serious error. The statement regarding the change in moisture content of the soil with depth is very helpful. As we normally bury the pipes at least 3 feet, it means we can expect fairly constant soil conditions. This may account for the small variation in soil thermal resistivity shown in Figure 11.

This paper is going to be very helpful to the insulated conductor committee of AIEE, which is studying this earth thermal resistance problem. It is not an easy one, as there are so many factors involved. However, in time, we can hope for a clearer understanding of what takes place and how to take care of it.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): Sanderson, Sticher and McGrath have made a very valuable contribution to the data required for determining ratings of pipe cables. Their data on earth thermal resistivity is very valuable since this has been a very controversial subject and requires settlement if over-all economy is to be obtained on underground systems.

We are conducting tests somewhat similar to those described in the paper. A test cell and an experimental length of pipe have been installed in our Queens district and over a period of approximately a year's operation the following values obtained.

Experimental Pipe	53 C/W/CM^3
Soil Cell	68 C/W/CM^3

From data obtained from a heat run on the Jamaica–Valley Stream 138-kv pipe line we obtained a value of 55 to 65 C/W/CM^3.

It is stated that the ratio of all a-c loss effects to the conductor d-c loss amounted to 33 per cent for 3-phase operation and 24 per cent for single phase operation. From tests made with three 350,000-circular-mil 138-kv cables in a 6 inch outside diameter pipe we found that for 3-phase operation the following values were obtained; $19^1/_2$ per cent at 30 degrees centigrade $17^1/_2$ per cent at 55 degrees centigrade.

C. T. W. Sutton (Enfield Cables Limited, Brinsdown, Enfield, Middlesex, England): The most interesting information gleaned from the data given in the paper are the thermal resistivity values actually computed from the results of tests, especially those suggested for the pressure medium.

In the early stages of the development of the compression cable system many investigations of similar character to those described were undertaken both here in Britain and on the continent of Europe and it is pleasing to find that the results we originally obtained are of the same magnitude as those now given.

We considered that it was of sufficient accuracy to assume a fixed thermal resistance value for the gas in the pipe irrespective of conductor size or dimensions of pipe and not to attempt to calculate the value for individual cases, especially as the thermal resistance of the ground, always an uncertain quantity, constitutes the major portion of the total resistance. Thermal resistivities of the gas calculated from temperature reading can only be valid for a particular set-up because the relative influences of conduction, convection and radiation vary.

We found that the configuration of the cable construction had a marked effect on the thermal resistance of the gas space and from the results obtained by several investigators the following thermal resistances were used for the current rating determinations.

1. 3 core screened	20 degree centigrade per watt per centimeter or 0.66 degree centigrade per watt per foot
2. 3 single core cables	10 degree centigrade per watt per centimeter or 0.33 degree centigrade per watt per foot

This confirms the variations in the thermal resistances that were noticed when the pipe was rotated through 120 degrees, especially as it appears from the information given in Table III that these particular tests were conducted with three cables of different dimensions enclosed in the same tube.

Values as low as 400 degrees centigrade per watt per centimeter cube for the resistivity of the dielectric of compression cables with normal gas pressure have been measured but for calculations of current

rating a value of 500 is assumed, while for gas-filled insulation a figure of 750 has been agreed.

The pipe covering type *A* given in Appendix I appears thick and figures are not given for types *B* and *C*. We have found from several years of operating experience that adequate protection against corrosion, electrolytic or otherwise, can be guaranteed by coating the pipes with the rubber sandwich protection (British Patent number 425906) which when applied by machine has a radial thickness of approximately $^5/_{16}$-inch and consequently its thermal resistance is very low, that is, in the region of 0.13 degree centigrade per watt per foot for a resistivity value of 500.

Referring again to Table III, is it not possible that the thermocouple readings were correct for the cable with the 500 mil dielectric thickness, because impregnated paper has a conductivity of seven times that of the gas, although the margin would of course be reduced due to convection and radiation in the gas space, especially convection.

The following thermal resistance values are typical of those used for obtaining current rating data for compression cables.

Cable: 0.5 Square Inch, 3-Core "S L", Pipe-Line Type 132-Kv, 3-Phase Working Pressure

	Thermal Resistivity °C/watt/cm²	Thermal Resistance °C/watt/cm	Per Cent
Dielectric thickness, 0.40 inch	500	50.5	
Diaphragm sheath reinforcements			
Oiled papers	750	1.1	
Oiled cotton tapes	500	1.3	
3 Cores in parallel	17.6		18.5
Gas	10		10.5
Pipe protection			
Rubber sandwich, Thickness 0.145 inch	500	3.7	4.0
Soil—depth 42 inches to top of pipe	120	64	67
Total		95.3	100

Finally it was interesting to note that electrical failure considered the result of faulty registration of the papers, or moisture occurred in types *A* and *D* cables at a stress of 264 volts per mil which corresponds to 10.6 kilovolt per millimeter. This is not an unduly large value for the stress especially as compression cables are working at stresses as high as 150-kilovolt per centimeter. These figures clearly show that cable designs containing gas pressure in the active dielectric are always liable to fail due to manufacturing weaknesses, as the factor of safety is not extremely high.

J. H. Neher (Philadelphia Electric Company, Philadelphia, Pa.): The thermal resistances involved in the determination of the temperature-rises in pipe-type cable systems depend upon so many interrelated variables that it is impractical to completely cover all possible combinations which may be met in practice by test. For this reason some of us have been actively endeavoring to establish formulas by which these resistances may be calculated.

While this development is based primarily on theory, the resultant formulas must be co-ordinated with such tests as are available, and to this end the very complete data presented by the authors will be of very great benefit. Since I have not had a chance to examine these data prior to this meeting, my remarks here must be quite general.

I note that the authors indicate a figure of about 850 degrees centigrade, square centimeters per watt, as the surface resistivity of the pipe in air, and compare this figure with that of 1,200 given by the Insulated Power Cable Engineers Association. To understand the mechanism of heat transfer involved, one must think in terms of conductance rather than resistance, and realize that this conductance is composed of a convection and a radiation component. The radiation component is dependent directly upon the surface emissivity constant, which is highest for a black painted surface. It so happens that the figure of 850 agrees not only with the theoretical value for a black pipe of this size but also with such other test results as I have observed. The figure of 1,200, on the other hand, is presumably based on a tarnished lead sheath where the emissivity is about half that of the black pipe, resulting in the correspondingly higher value of resistance.

In the case of the heat flow between cable surface and the pipe, there is an additional conduction component which must be added to the convection and radiation components. The way in which these components will vary and their relative effect on the over-all conductance will depend upon the type of medium filling the intervening space. Thus when the medium is oil, the radiation component is relatively insignificant and the convection component will vary as the 3/4 power of the oil temperature. If the medium is gas, the conduction component is relatively small and the convection component now varies theoretically as the square root of the absolute pressure.

Finally, the authors have indicated an observed value of roughly 50 degree centigrade, centimeters per watt, for the thermal resistivity of the earth by back calculation from the cable tests as contrasted with about 65 as observed by a test cell installed at the same location. This apparently large discrepancy may be due to the fact that the temperature-rise of the cable system had not reached its ultimate value at the time of the observation. Due to the very long temperature-time transients involved, it is often impossible to establish the ultimate temperature-rise by merely plotting the temperature-rise against time with rectangular co-ordinates. We have found the following procedure to be more advantageous in this connection.

The measured temperature-rise per watt per foot $\Delta T/Q$ is plotted against time on log-log paper. The theoretical temperature-time transient, which may be obtained with sufficient accuracy from an equation given in the Whitehead and Hutchings paper cited by the authors,[1] is similarly plotted on a sheet of the same log-log paper, plotting $\Delta T/Q\rho$ against te/r^2 where ρ and e are the thermal resistivity and diffusivity of the earth respectively, and r is the radius of the pipe. The two sheets are then superimposed and the best match is obtained with the upper portion of the measured curve keeping the axis of the sheets parallel. The relative positions of the axis of the two sheets will then give ρ and e/r^2 directly. Since r is known e is readily determined.

Reference

1. Reference 6 of the paper.

A. D. Petzee (General Cable Corporation, New York, N. Y.): It is worthwhile to point out in Figure 12 of the paper the difference in power factor between cable *G* and cables *H* and *I*. All three cables were impregnated with synthetic oil with a viscosity of 3,000 Saybolt Universal seconds at 100 degrees centigrade.

Cable *G* was insulated with predried preimpregnated tapes in an atmosphere kept below a specified relative humidity, whereas cables *H* and *I* were taped dry and mass-impregnated as with a solid type cable, which permits drying and impregnation of the insulation after taping, and without exposure to the air.

While the power factor of the preimpregnated tape cable is adequately low, the power factor of the mass-impregnated cable is inherently lower and permanently so on account of its lower moisture content.

These experimental cables, by the way, represent the first public use of high voltage cable mass-impregnated with a compound of viscosity 3,000 Saybolt Universal seconds at 100 degrees centigrade, which is 20 to 30 times that used with "solid" cable.

W. D. Sanderson, J. Sticher, and M. H. McGrath: The authors are very much pleased with the interest shown in this paper and wish to thank the engineers who have submitted discussions. In general, these discussions confirm the data presented. Wiseman takes the results shown in Table II to be representative of a cable assembly using gas and oil respectively as the pressure medium. This particular investigation was concerned with the heat flow through the compression medium, and other data which were readily obtained during the test were included in the table.

Inasmuch as the cable used in this particular phase of the investigation was of the "solid" type, the resistivity value, shown for the insulation cannot be considered to be strictly representative of both the gas and oil-filled systems. However, the results obtained for the insulation with gas as a pressure medium are quite close to those obtained with the actual *SMD* cables as shown in Table III. Concerning the results obtained with oil as a pressure medium, it must be pointed out that the likelihood of incomplete insulation saturation seems extremely unlikely because in both the 100 and 200 pounds per square inch tests, the setup was held at approximately 70 degrees centigrade for more than four hours. At these pressures and at this temperature, complete saturation of the insulation with an oil having a viscosity of 700 seconds at 100 degrees Fahrenheit should be effected in a fraction of this time.

Sutton mentions that in England a figure of 750 has been agreed on for gas-filled insulation. In 1942 Beaver[1] reported a value of 700. The average thermal resistivity of low pressure (9–15 pounds per square inch gauge) gas-filled insulation has been given as 700 by Shanklin.[2] Inasmuch as the insulation of both the Beaver cable and the

low-pressure gas-filled type is impregnated with a saturant which has a viscosity at 100 degrees centigrade of only three to five per cent of that used in the gas-filled cables listed in Appendix II of the paper, it seems reasonable to expect the loss of saturant from the paper fiber in process and in service to be materially lower for these latter cables particularly as, in addition, a relatively large portion of the insulation wall is composed of superdense and high density paper. It is believed that this is also the explanation for the relatively low thermal resistivity of the gas-filled cable designs used in the tests described in the paper.

Permanency of the initially-low thermal resistivity is indicated by the absence of any significant change in the specific inductive capacity after the experimental field runs or after severe drainage tests. In this connection it is pertinent to note that, while Beaver reports a permittivity of 2.9 to 3.0 for the English gas-filled cable, the gas-filled types of cable listed in Appendix II have permittivities very closely the same as for a "solid" or low-pressure oil-filled cable with substantially the same tape structure.

Sutton wants to know if it is not possible that the loose thermocouple on the 500-mil sample of Table III gave correct results and goes on to explain the well known fact that dry paper fiber has a lower thermal conductivity than impregnated fiber. As pointed out before, the paper fiber in question is impregnated and remains so in service. It is not clear as to just what Sutton is attempting to prove because he seems to have overlooked the fact that the measured conductor temperatures on the 500 mil with the loose thermocouple were lower than expected, which means that if the values had been accepted as correct, the calculated thermal resistivity would have been lower, not higher, than given in Table III.

Sutton gives a tabulation of a set of thermal resistivity values and typical thermal resistances (it might be pointed out, for the sake of the record, that in the tabulation the heading of the second column of figures should be thermal resistance) for a 0.5-square-inch compression cable with 400 mils of insulation; the dimensions of the pipe are not given. It is interesting to note, however, that if Sutton had employed a soil resistivity of 50 (which value approximates that indicated in the test runs covered by Table V) the total thermal resistance from conductor to soil ambient for Sutton's compression cable would be within two per cent of that given in the section of the paper captioned "Thermal Resistances—Conductor to Sink." Sutton does not mention any correction for the sheath losses in the compression cable. If these sheath losses would be included Sutton's compression cable would have a greater temperature rise from conductor to sink, and, consequently, a lesser current rating for the same soil resistivity.

The rubber sandwich-type of pipe corrosion protection mentioned by Sutton was considered here some years ago but was not employed because of its relatively high cost. It should be noted that the thermal resistance figure of 0.13 degree centigrade per watt per foot of circuit given by Sutton for a 3/16-inch covering is 0.39 degree centigrade per watt per foot of conductor. The latter value is not materially different from the 0.48 value given in "Thermal Resistances—Conductor to Sink," section of the paper for a 7/16-inch covering.

It is interesting to note that Wiseman's calculations of the thermal resistance across the gas zone, using 500 as the surface resistivity, is practically identical to that obtained from the test results. However, he states that his calculated thermal resistance across the oil zone would be only 40 per cent of the value obtained from the test with oil as the pressure medium. Wiseman expresses the view that this is due to the use of an oil having a higher viscosity than that which his computed results would apply. The oil used was supplied by the Okonite-Callender Cable Company and was known as Oilostatic oil or 700-second oil. Since Wiseman's calculations are based upon a surface resistivity which was determined by test, it would be very helpful if Wiseman would present this data on the thermal characteristics of Oilostatic-type cable in the near future. In all the laboratory tests conducted by the authors, the surface resistivity values obtained have never approached the low values reported by Wiseman.

The variation in the surface resistivity from cable to pipe as shown in Table III was due to the changes in the geometric arrangement of the cables with respect to the pipe, resulting from the rotation of the assembly. The cables were bound in a triangular configuration before being placed within the pipe. This allowed the various cables to become suspended in the pipe as the assembly was rotated. It was interesting to learn that Sutton's investigation confirmed the variation found in the surface resistivity from cable to pipe.

Sutton states that it is sufficiently accurate to consider the thermal resistance from cable surface to pipe wall through the gas medium as a constant irrespective of cable dimensions. For three single conductor cables in pipe Sutton suggests a constant value of 0.99 degree centigrade per watt per foot of conductor. We cannot subscribe to Sutton's point of view because reference to Table III shows that his proposed constant is from 11 to 97 per cent below the measured values. Sutton argues that accuracy is not required in this term because it is small with respect to the earth term. However, he apparently uses a soil resistivity of 120 whereas in this country 70 to 80 is customary. For example, if one should assume that he in his tabulation, had underestimated the thermal resistance from cable surface to pipe by 100 per cent, then for a soil resistivity of 80, his final rating would be approximately seven per cent high.

Wiseman and Neher have commented on the surface resistivity from pipe to air. In this connection it should be recalled that this particular investigation was undertaken to study the thermal characteristics of the path through the pressure medium as affected by the position of the cables in the pipe. Here again, other data that were readily obtained were included, such as those covering the heat flow from the pipe surface to air. One explanation of the variations mentioned could be that the welding generators used to provide the necessary direct current had built in fans for cooling and it is very likely that these, along with the building air-conditioning system, could have set up a breeze to cool the pipe assembly.

We were very pleased to learn that Hatcher's soil resistivity data confirmed those obtained in Detroit and that he will continue tests along this line. We are looking forward to early publication of the results. With respect to Neher's comment on the back calculations of soil resistivity and long-time transient effects, an alternate and somewhat simpler approach to the transient problem is to employ the solution for a continuous line source of constant strength. On this basis the long-time transient thermal resistance from the surface of the covering to earth sink with constant load may be written as:[3]

$$R_{(t)} = \frac{\rho}{2\pi}\left[\log_e \frac{4l}{D} - \frac{1}{2}(F(V') - F(V))\right]$$

where the symbols and units except for those indicated below, are the same as given in the text.

$$V' = \frac{(2.54)^2 l^2}{\xi t} \qquad V = \frac{(2.54)^2 D^2}{16 \xi t}$$

ξ = thermal diffusivity in cm²/sec
t = time of application of constant load in seconds

$$F(x) = x - \frac{x^2}{2 \times 2!} + \frac{x^3}{3 \times 3!} - \frac{x^4}{4 \times 4!} + \ldots$$

For values of x above 3.0, $F(x)$ may be computed from $F(x) = 0.577 + \log_e (x)$.

The attainment factor or the ratio of the pipe covering surface temperature rise (above ambient at depth of burial of pipe axis) at time "t" to the ultimate temperature rise is then:

Attainment factor

$$= 1 - \frac{(F(V') - F(V))}{2 \log_e \frac{4l}{D}}$$

Gemant and Sticher[4] have made a careful estimate of the diffusivity of soil in the vicinity of the experimental line for soil having a resistivity of 72 as determined by the soil cell. Considering a run such as test period 8 where the time under constant load was of the order of 1,000 hours and using the Gemant and Sticher estimate of 4.2×10^{-3} centimeter square per second for the diffusivity, it is found that for a 54-inch depth and a covering diameter of 7 7/8 inches $V' = 1.25$, $V = 1.7 \times 10^{-4}$, $F(V') = 0.94$, $F(V) = 0$, and $2 \log_e \frac{4l}{D} = 6.6$. Hence the attainment factor is 0.86. This means that if the ultimate resistivity were 72, the apparent value at the end of 1,000 hours would be 62. It is interesting to note that the ratio of soil cell to line resistivities as given in the tabulation in the paper is 0.79. This consideration shows that values derived from the cable test are about 14 per cent too low; the true value appears to lie between those derived from the cable and from the cell.

The foregoing method could also be used to determine ultimate resistivity and diffusivity directly from measured values of temperature rise and time on the line pipe by use of simultaneous relationships.

Wiseman's comments on the soil temperature data in the vicinity of the loaded pipe recalls the assumption proposed by Apt[5] some years ago. Apt suggested that as a first approximation the effect of the earth surrounding the cable could be con-

sidered to be equivalent to that of a cylinder of material having a thermal resistivity equal to that of the soil and having a radius equal to the depth of the cable axis below the surface of the ground. This assumption was considered by the British[6] in 1923 but was not adopted by them because an experimental investigation on the effect of depth of burial showed the Kennelly assumption to more nearly represent the observed facts.

Hatcher's supplemental information concerning the ratio of all a-c loss effects to the d-c loss was welcomed by the authors. Further work needs to be done on this particular phase of the pipe-type system in order to be able to break this extra loss up into its components. In preparing Table V, 61 per cent of this extra loss was considered to be in the iron and the remaining 39 per cent to be additional conductor loss. We are aware that the above percentages are not absolutely correct, but they do give thermal constants which are slightly on the conservative side.

The authors appreciate the comments by Pettee who is associated with one of the authors. This information is additional to that given in the paper, and might well have been included in it. However, an attempt was made to limit information as much as possible to that more closely related to the thermal characteristics of the cable installation.

Sutton's assumption that the electrical failures in the type *A* and type *D* experimental cables were the result of faulty registration is incorrect. As clearly indicated in the paper, the failure on the type *A* cable, which was the first experimental *SMD* cable ever made, occurred during overload tests in connection with thermal studies, as the result of cumulative dielectric loss due to moisture absorbed during manufacture with preimpregnated paper tape applied in open room air without humidity control of any kind. The importance of such control was appreciated fully at the time this initial experimental cable was constructed and was not employed only because of existing time limitations. Space does not permit a discussion of the pertinent dissection data except to state that the field examination, the observed load and temperature conditions supplemented by laboratory examination and tests on samples adjacent to the fault disclosed conditions that fully substantiate the ascribed cause of failure.

In the case of the type *D* experimental cable, the irregularities in taping associated with the experimental use of 1.5 mil tape in the inner portion of the insulation referred to in the paper and which Sutton has erroneously interpreted to mean "faulty registration" were actually an accidental longitudinal foldover in the innermost 1.5 mil tape, and longitudinal wrinkling, extending through all 1.5 mil tapes. Both failures on the type *D* insulation were related to the thinness of the tapes used and to the very low mechanical tension required in their application as well as to the effect of the relative thinness of the wall as a whole.

The commercial cable type *G*, was designed to be entirely free of ionization up to about twice operating voltage. In the absence of ionization, the cable should operate indefinitely and there should be no need for a higher factor of safety. Examination of a length of the commercial cable removed after approximately five years of operation revealed no significant change in the degree of impregnation, the appearance of the insulation, the dielectric strength or the power factor-temperature characteristic.

In conclusion, the various tests conducted by the authors indicate that the following thermal resistivities may be used as a fair working basis for design calculations:

Paper insulation	550	°C/W/CM³
Pressure medium (Surface resistivity from cable surface to inner wall of the pipe)		
Gas	500	°C/W/CM³
700-second oil at 100° Fahrenheit	400	°C/W/CM³
Pipe coating *A*	100	°C/W/CM³
Pipe coating *D*	71.5	°C/W/CM³
Clay soil	70	°C/W/CM³
Sandy soil	85	°C/W/CM³

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5. Heating of Underground Cables, **R. Apt.** Electrotechnische Zeitschrift (Berlin, Germany), 1900, volume 21, page 613.

6. Second Report on the Research on the Heating of Buried Cables, **S. W. Melsom, E. Fausett.** *Journal*, Institution of Electrical Engineers (London, England), volume 61, 1923, page 517.

Magnetic Recording Tapes

MARVIN CAMRAS
MEMBER AIEE

Synopsis: Paper or plastic tapes with a magnetic coating, as well as solid metal ribbons, have a wide field of use in magnetic recording. Magnetic properties, frequency response, signal-to-noise ratio, output level, sensitivity, and erasability of a number of different tapes are compared. A newly developed coated material has a balance of these properties which makes it especially suitable for high quality magnetic recording.

THE MOST COMMON magnetic-recording tape is a paper or plastic ribbon with a thin layer of permanent magnet material on its surface. Paper tape is economical, gives good performance at low speed, and is easy to handle. It is not as durable as wire and is considerably bulkier, but for many applications these qualities are not important. As with other magnetic record media, the tape can be erased and reused. Figure 1 gives the dimensions of some typical recording tapes and wires for comparison.

In comparing different recording tapes, the ultimate test is the performance in actual use. For speech or music reproduction the performance will depend on frequency response, background noise, distortion, and so forth. But these performance factors are influenced not only by the record medium, but also by the erase, record, and pickup heads; by the kind and amount of bias; by the amplification and equalization; by the tape speed; by the cross section of the magnetic material; and by many other factors. To evaluate the quality of the tape material independently of these influences, it is useful to study such things as magnetic properties, which can be correlated to the performance factors.

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MARVIN CAMRAS is physicist with Armour Research Foundation, Illinois Institute of Technology, Chicago, Ill.

Magnetic Properties of Metal Tapes

Steel tapes were used in the early days of magnetic recording. Figure 2 shows the hysteresis loop of a cold-rolled steel tape. All of the oscillograms were made on a curve tracer as described elsewhere.[1] The high residual induction ($B_r = 10{,}800$) gives high output, especially at low frequencies; but the low intrinsic coercive force ($H_{ci} = 10$) indicates poor high frequency response, unless excessive tape speeds are used. Heat-treated steel has somewhat better magnetic properties as indicated by Figure 3. Chrome steel and tungsten steel were also used, but the improvement over carbon steel is relatively slight. While other permanent magnet materials such as "Alnico" have excellent magnetic properties, they are too hard and brittle to be made in tape form. An important advance was the development of "Vicalloy" which had an intrinsic coercive force of 260 and a residual induction of 7,800 as shown by Figure 4.[2] "Vicalloy" tape at present is too expensive for widespread use in such applications as home recorders.

A metallic tape of much lower cost can be made from a stainless-steel alloy similar to that used for round recording wire. Stainless-steel tapes can be run on machines designed for coated tapes. Hence, they can be used for important recordings which should be more permanent than paper-tape records. The durability and corrosion resistance of stainless tape also makes it attractive for advertising devices, announcing systems, and other applications where the material is continually reused, and where weaker material would wear out. Magnetic properties of a typical stainless tape are shown in Figure 5.

The magnetic properties of various metal tapes are summarized in Table 1.

Magnetic Properties of Coated Tapes

One of the first materials tried for coated-magnetic tapes was carbonyl iron which was used on early German tape machines. Carbonyl iron is a fairly soft magnetic material, as shown by Figure 6, with an H_{ci} of 5 and a B_r of 100. As might be expected it makes a poor record medium, both in output and in frequency response.

It was later found that magnetite powder gave improved results, and tapes coated with this material were used on German machines manufactured in the late 1930s. The H_{ci} of magnetite runs as high as 100 to 150, and is responsible for the better performance. Magnetic properties of magnetite-coated tape, of American manufacture, are given in Figure 7.

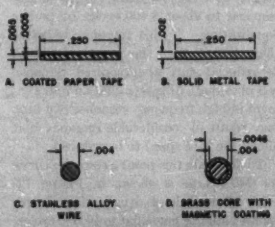


Figure 1. Cross section of some magnetic recording media

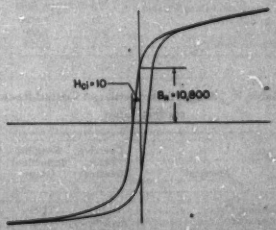


Figure 2. Hysteresis loop of cold rolled steel tape

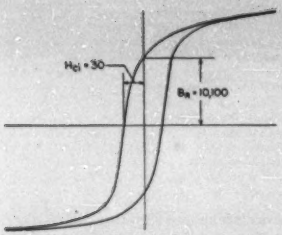


Figure 3. Hysteresis loop of heat treated carbon steel tape

The highest quality German tapes, which were developed during the war, were made with gamma iron oxide, and a vinyl chloride base material. In one form the magnetic particles were dispersed throughout the carrier to give a homogeneous tape. In the other form, the magnetic layer was deposited on one surface of the tape. Only a limited amount of magnetic matter can be incorporated into the homogeneous tape without serious loss in tensile strength. The homogeneous tape, therefore, has lower B_r than the coated tape. The lower B_r is counteracted to some extent by the thicker layer of active material. Magnetically the gamma iron oxide has properties similar to magnetite, as can be seen from the hysteresis loop of Figure 8.

An experimental tape was coated with vectolite material, made from 30 per cent Fe_2O_3, 44 per cent Fe_3O_4, and 26 per cent CO_2O_3. This is a high grade permanent magnet material with H_{ci} of 880 oersteds, and a B_r of 570 gauss, as shown by Figure 9. (This coercive force was found to be too high. It makes the tape difficult to record, and almost impossible to erase.)

The best all-around coating was one developed at Armour Research Foundation and designated as *140A* material. It has a B_r of 800, and H_{ci} of 340; and is essentially saturated at an applied field of 1,000, which makes it easy to erase. Magnetic properties of the *140A* material are given in Figure 10.

A summary of the properties of coated tapes is given in Table II.

Table I. Magnetic Properties of Metal Recording Tapes

Material	Coercive Force, Oersteds	Residual Induction, Gauss
Carbon steel (cold worked)	10	10,800
Carbon steel (heat treated)	30	10,100
Chrome steel	52	7,000
Vicalloy	260	7,800
Stainless alloy	245	1,725

Table II. Magnetic Properties of Coated Recording Tapes

Material	Coercive Force, Oersteds	Residual Induction, Gauss
Carbonyl iron	5	100
Magnetite	120	375
Gamma iron oxide	110	300
Vectolite	880	570
Armour *140A* material	340	800

Performance Characteristics of Tapes

Probably the most important performance characteristics of tapes are

1. Frequency response characteristic.
2. Maximum signal-to-noise ratio.
3. Output level.
4. Sensitivity.
5. Erasability.

These will be considered separately.

Frequency Response Characteristic

The frequency response characteristic determines the fidelity at a given speed.

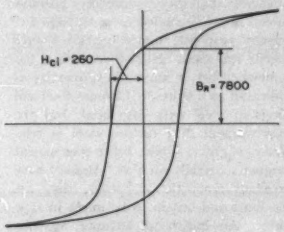


Figure 4. Hysteresis loop of Vicalloy tape

A typical constant current response curve for tape is shown in Figure 11. Since it is ordinarily not advisable to equalize the high frequency response more than about 15 decibels, we would say that the tape of Figure 11 could be made to give a flat response to about 8,400 cycles by proper equalization. If this response is better than needed, then the tape speed could be reduced until the required response was obtained. It is seen that an improvement in high frequency response of a tape may result in considerable economy if it allows the tape speed to be reduced.

To illustrate this point a response curve for *140A* tape is shown in Figure 12. With 15 decibels of equalization the response could be made flat to 10,000 cycles. Compared to the tape of Figure 11, the *140A* material could be run 84 per cent as fast for the same response. However, it should be noted that frequency response, though important, is not the only consideration, and other characteristics may determine the final worth of a recording tape.

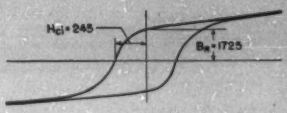


Figure 5. Hysteresis loop of stainless alloy tape

Signal-to-Noise Ratio

Signal-to-noise ratio determines the dynamic range of the record medium and the amount of background noise on speech or music reproduction. The type of noise and noise-frequency distribution provides an important limitation on the amount of equalization, so that indirectly the signal-to-noise ratio may affect the frequency response. One advantage of magnetic recording is the low background noise.

For a true measure of signal-to-noise ratio, the tape should be operated in circuits which have the same equalization and band width as those used in ultimate operation. Since conditions of ultimate operation will vary according to use, a signal-to-noise figure should be accompanied by a description of the conditions under which it was taken. For purposes of comparing the various media, the signal-to-noise ratio was determined under conditions of constant recording current, and playback through an amplifier flat from 20 cycles to at least 20 kilocycles. Recording and playback speed was one foot per second, and the tape channel width was 0.125 inch. The maximum signal level was taken as the output decibels for saturation recording intensity at the frequency of maximum response. The noise level was taken as the output of an erased tape under the same conditions.

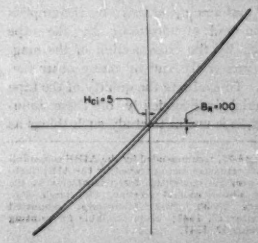


Figure 6. Hysteresis loop of carbonyl iron coated tape

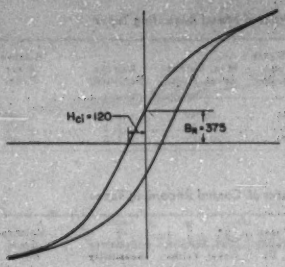


Figure 7. Hysteresis loop of magnetite coated tape

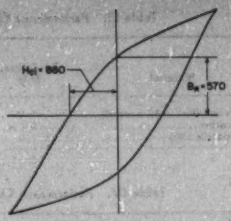


Figure 9. Hysteresis loop of Vectolite coated tape

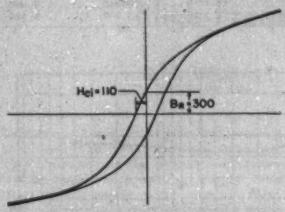


Figure 8. Hysteresis loop of gamma iron oxide coated tape

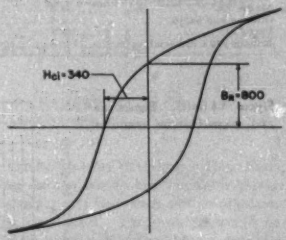


Figure 10. Hysteresis loop of *140A* coated tape

Tables III and IV compare the signal-to-noise ratios of various tapes tested. It should be noted that in some cases the noise level is so low that it is partially masked by vacuum tube noise of the preamplifier. In these cases, the signal-to-noise ratio is specified as at least that number of decibels.

In addition to the "erased noise" heretofore described, there is also a noise which varies with the recorded signal, and has been termed "modulation noise."[3] If this noise is kept below a certain level, it is marked by the signal, and is not considered troublesome.

Output Level

A high output level from a recording tape is desirable for several reasons. Less amplification is needed, and in some cases an amplifier stage may be omitted. A narrower track can be used, allowing economy of material. Hum level and tube noise are reduced at the lower amplification, thus improving the over-all signal-to-noise ratio.

For comparison among various tapes the maximum signal output level at frequency of maximum response is tabulated in the third column of Tables III and IV. The output voltage in all cases was determined for a 0.125 inch wide track and tape speed of one foot per second, with adjustments made so as to give the best performance for each tape.

The metallic tapes have high outputs, mainly because the active material is thicker than on coated tapes. On the other hand, the maximum output of coated tapes occurs at a higher frequency, and at high frequencies the voltage from the coated tape may exceed that from the solid metal tape.

Sensitivity

Sensitivity is defined by the equation

$$S = K \frac{V_0}{I_1}$$

where

S = sensitivity
V_0 = output voltage on playback
I_1 = recording current
K = a constant depending on test apparatus and conditions

A typical curve of output voltage versus input current is shown in Figure 13. Since this function is practically linear below overload levels, the sensitivity of a given tape is substantially constant. In general, the sensitivity will vary with the frequency and with the bias adjustment. For comparison purposes the frequency and bias were adjusted to give maximum sensitivity on each of a number of tapes. The results are tabulated in column four of Tables III and IV.

The metallic tapes are generally more sensitive than the coated tapes because of their greater output. Of the coated tapes, it would be expected that low coercive materials would be most sensitive. The *140A* material turns out to be more sensitive than the others; though its coercive force is relatively high, the B_r rises rapidly after a critical value of field is reached.

Erasability

One of the important advantages of magnetic recording is that the record may be erased if desired, and a new record made in its place. To insure that a magnetic record will be erased completely, it must be subjected to a field high enough to produce magnetic saturation. This is readily done if a permanent magnet or a d-c field is used. However, a tape erased with direct current is likely to be noisy; also it will have even order distortion if a subsequent attempt is made to record by the supersonic bias method.

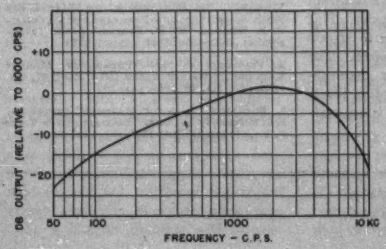
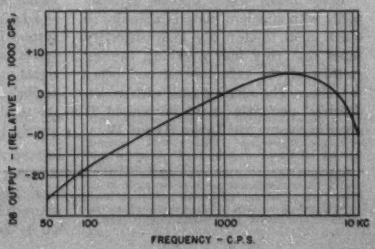


Figure 11 (Left). Constant current response of stainless alloy recording tape

Figure 12 (Right). Constant current response of *140A* recording tape



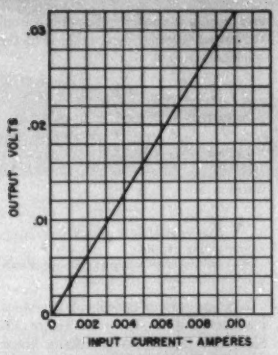


Figure 13. Linearity of output voltage versus input current at low levels

Figure 14 (left). Experimental magnetic-tape recorder

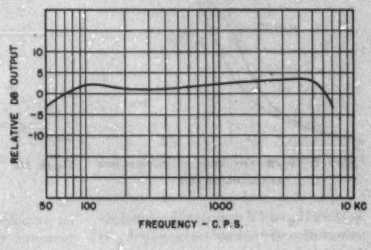


Figure 15. Over-all response of experimental recorder

For best results the tape is demagnetized by an a-c erase magnet. The ease with which a material erases depends not only on its coercive force, but also on how easily it saturates. Some materials reach most of their B_r at a low field, but saturate slowly, the B_r continuing to rise even for fields of several thousand. These materials are difficult to erase. Others saturate at low fields even though they have relatively high coercive force.

In general, materials with a coercive force below 200 are easily demagnetized. Those with a coercive force of 350 to 400 are intermediate. Tapes with coercive forces above 400 are difficult to erase.

Erase heads which operate satisfactorily with standard materials require so much input to clean the high coercivity tapes that they may overheat, causing melting of the tape coating and other troubles. The relative ease with which different materials may be erased is given in column 5 of Tables III and IV.

Table III. Performance Characteristics of Metal Recording Tapes

Material	Thickness, Mils	Max. Signal to Noise Ratio, Db	Max. Output Level, Volts	Relative Sensitivity	Relative Erase Power
Carbon steel (cold worked)	1.0	44	0.050	6.1	0.025
Vicalloy	2.0	62	0.120	5.2	0.090
Stainless alloy	1.0	62	0.075	1.2	0.160

Table IV. Performance Characteristics of Coated Recording Tapes

Material	Thickness, Mils	Max. Signal to Noise Ratio, Db	Max. Output Level, Volts	Relative Sensitivity	Relative Erase Power
Carbonyl iron	1.2	34	0.001	0.016	0.250
Magnetite	0.5	58	0.020	0.390	0.130
Gamma iron oxide	0.6	64	0.036	1.400	0.032
Vectolite	0.8	60	0.090	0.810	2.000
Armour *140A* material	0.5	68	0.120	5.300	0.190

Experimental Recorder

Figure 14 shows an experimental recorder designed to take advantage of the properties of the *140A* tape material. The tape is stored on an 8-millimeter movie reel, which will hold programs up to an hour in length. Two channels are used on the one-fourth inch wide tape. When the tape passes from the right-hand to the left-hand spool, the upper channel plays. When it drives in the opposite direction, the lower channel is in the circuit. Thus, no rewinding is necessary, and a program is always available.

Over-all frequency response of the system is shown in Figure 15. At eight inches per second the output is flat within ±3 decibels from 50 to 7,000 cycles. This system is capable of response to 10,000 cycles at the same speed if desired. The signal-to-noise ratio is better than 45-decibels, and at normal recording levels the total harmonic distortion is approximately 1.0 per cent.

Conclusions

A large number of physical and magnetic properties must be considered in evaluating the suitability of a material for a magnetic recording tape. Solid metal tape is best for durability and repetead use. Coated paper or plastic tape is not as permanent, but is lighter, easier to handle, and less expensive.

The best coating material for tapes has high coercivity and retentivity. It should saturate at relatively low fields for good erasing.

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No Discussion

A Turn-In-Gap Erase Head for Magnetic Recorders Providing Intense High-Frequency Fields

D. E. WIEGAND
MEMBER AIEE

R. E. ZENNER
NONMEMBER AIEE

Synopsis: A new erase head structure has been devised for use in magnetic recording. This new head provides, at reasonable power inputs, much more intense erasing fields than were possible previously and thus makes the use of magnetically harder media feasible. This head is excited by a single conductor in the erasing gap. The gap conductor is energized by a single turn secondary winding of a transformer. Greater efficiency is achieved through the resulting reduction in head flux for a given magnetomotive force applied to the medium. Also, improved thermal characteristics increase the power handling ability of this head.

IN A MAGNETIC recorder, remanents of previously recorded signals not completely erased contribute to the background noise level of a new recording. Particularly in a high quality recorder, it is desirable to keep the background-noise level as low as possible. In typical low cost home recorders stray hum pickup, amplifier microphonics, and similar disturbances rather than wire noise are controlling factors in the over-all background noise of the machine. With presently used magnetic recorder media a degree of erase entirely satisfactory under these conditions is obtained easily with conventional head structures. In a high quality machine, however, these other disturbances are reduced sufficiently that essentially only the wire noise remains. To realize the benefits of this low random background noise level requires that the erase head be able to completely erase previously recorded signals. With the tendency toward media of higher and higher coercivity this has often been quite a difficult problem with conventional erase head structures. Head heating and relatively high oscillator power requirements have been serious problems in some cases. The problem would be much less severe if the old d-c erase d-c bias (Poulsen) system[1] were used. The modern a-c erase a-c bias system[2] is greatly to be desired, however, because of its greater dynamic range and lower distortion.

The improved frequency response characteristics possible with a medium having high coercivity[3] explains the tendency toward these magnetically harder materials. The turn-in-gap erase head design provides high-frequency fields of sufficient intensity to completely erase such media. Moreover, the improved efficiency of this design make the oscillator requirements less severe.

The Turn-In-Gap Erase Head Design

The usual high-frequency erase head consists of one or more laminations forming a magnetic circuit which is closed except for a narrow gap. An exciting winding is wound around part of this magnetic circuit. The recording medium travels over a portion of the magnetic circuit including the gap. When the coil is excited by a high-frequency current, a magnetomotive force is developed across the gap and, therefore, across the length of medium over the gap. The gap is so proportioned that an element of medium is subjected to several reversals of the high-frequency flux while it is over the gap, and so that the field strength dies out gradually on both sides of the gap. Erasing action, therefore, is similar to the commonly used method of demagnetizing a ferromagnetic object with an a-c field. Since the permeability of the recording medium under erasing conditions is very low, 3 or less, it is evident that only a small portion of the flux in the core gets into the medium.

The magnetic flux in the head core is equal to the sum of the flux in the medium plus various stray (nonuseful) fluxes. In a typical erase head, the power input is determined mainly by hysteresis and eddy losses in the head core. Copper losses in the exciting winding usually are much smaller. It is evident, therefore, that if the nonuseful fluxes for a given gap magnetomotive force are reduced, a significant gain in erase head efficiency will result, through the reduction in operating flux density in the head core. In electromagnetic apparatus in general, nonuseful fluxes are minimized by placing the excitation of the system as near as possible the region where the magnetomotive force is desired. This region in an erase head is directly above the gap in the core.

The turn-in-gap erase head is excited by a thin copper strap in the gap directly below the recording medium. From the discussion above it is evident that, for a given magnetomotive force across the gap tips, the flux in the head core is significantly less than in structures which are excited by windings around one or more legs of the core. A further gain in efficiency results from the reduced mean length of flux path. The minimum path length is around the gap strap on the turn-in-gap head, rather than around the core window in a conventional head.

Efficient excitation of the head with an electronic oscillator requires the use of an impedance matching transformer. To

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D. E. Wiegand is senior physicist, and R. E. Zenner, supervisor, electricity and magnetism section, physics research, with the Armour Research Foundation, Illinois Institute of Technology, Chicago, Ill.

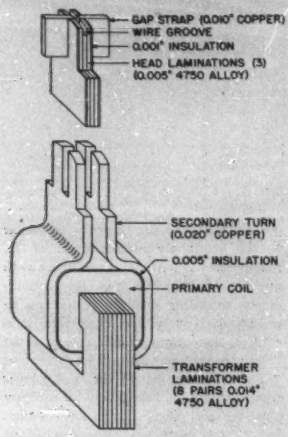


Figure 1 (left). Construction of turn-in-gap erase head

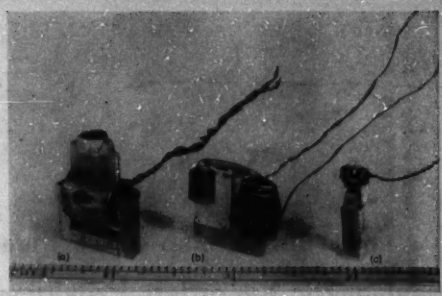


Figure 2. Turn-in-gap, combination, and solder-lug heads

minimize losses in the secondary circuit, the transformer and head are built as an integral unit in such a way that the length of the secondary circuit is a minimum. The unit is proportioned so that transformer losses and those in the secondary leads are small compared to the losses in the head itself.

Most of the development work on the turn-in-gap erase head has been on a design for use with round wire media and it is this design that will be described. The same principles were used in an erase head for magnetic tapes and were found to give good results in this case also.

The general construction of the turn-in-gap erase head is shown in Figure 1. The assembly of head laminations and gap strap shown at the top of Figure 1 is slid down between the facing surfaces of the secondary turn, the gap strap being fitted into the slots in the ends of the secondary turn. The secondary turn is then clamped over the surfaces immediately below the gap strap, after which the gap strap is soldered to the secondary turn around the edges of the slots. Finally, the bottom of the wire groove is rounded, and the ends of the groove are beveled and rounded for knot passing action.

The size of the turn-in-gap head relative to that of two other types is evident from Figure 2. Head *b* is a combination erase, record, and play-back head, *c* is an erase head in which improved efficiency was obtained by reducing the mean length of the magnetic path. This was done by reducing all dimensions to practical minima.

Erasing efficiencies of these heads were obtained by recording a signal at saturation level on a length of wire. By passing this wire over the erase head under study and measuring the remanent signal corresponding to varying currents in the erase head, data relating remanent signal level with erase current were obtained. A special play-back system described in the next section was used in these tests. The effective a-c resistance and inductance of each of the heads were measured with a Maxwell bridge.[4] This bridge was arranged to allow these measurements to be made under operating conditions of current and frequency, about 40 kc per second. Values of power input to the heads were calculated from the effective resistance values and values of current in the erase tests. In the case of the turn-in-gap head, the impedance measurement was made at the terminals of the primary winding. Losses in this winding and in the transformer core were thus included.

Characteristic of the Measuring System

In a high quality magnetic recording system remanent signals at the approximate level of the background wire noise will be audible and, therefore, objectionable. Measurement of remanent signals at this and lower levels requires the use of a tuned play-back system. Measurement of low level signals usually is made difficult by the interfering effect of noise generated in the first stage of the play-back amplifier. In the play-back system used in these tests this problem is made much less severe through the use of a play-back head–transformer combination providing a much higher signal to the amplifier input than usually is obtained. This was done by selecting a step-up ratio in the input transformer which gave the maximum peak signal on a series of frequency response curves, each for a different step-up ratio. This system has greater losses at high frequencies than would be tolerable in a sound recorder but the characteristics are suitable for the erase head study, since it is found that, in general, high-audio frequencies are more easily erased than medium or low frequencies.

The play-back system is shown schematically in Figure 3. Either broad band or tuned measurements can be made with this system. For broad band measurements, readings are obtained directly from the vacuum tube voltmeter. The vacuum tube voltmeter is provided with output terminals which allow its use as a calibrated amplifier. For tuned measurements these output terminals are connected to the input of a sound analyzer through a matching resistor. The sound analyzer is a frequency selective amplifier operating on the constant percentage band width principle. That is, the width of its resonance curve in cycles per second is proportional to the frequency setting of the tuning dial. The play-back system is calibrated to measure the voltage at the input of the 100/1 amplifier.

The characteristics of the play-back system are shown in Figure 4. The wide band play-back curve was obtained by measuring play-back voltages of a variable frequency recording which had been recorded with a constant current such that the play-back level was about nine decibels below saturation level. As mentioned previously, the loss at high frequencies is greater than would be tolerated in a sound recording system. The advantages of this high level system for measurements of this type are evident from a comparison of the saturated signal level, 81 decibels with the system noise level, five decibels. System noise includes all distrubances except that of the wire noise, such as hum pickup,

random noise and microphonics in the amplifiers. With this same wire in a high quality sound recording system it would be difficult to hold the system noise to a level 65 decibels below the saturation level. A further gain in measuring range is possible through the use of the tuned play-back system. In this case, the system noise is about 100 decibels below the saturation level. This is equivalent to a measuring range of 100,000 to 1 on a voltage basis or 10^{10} to 1 on an energy basis. This range is more than sufficient for the erase head measurements.

Recordings at 700 cycles per second were used in obtaining data for the erase head characteristics. This frequency is high enough so that, with the play-back system tuned to this frequency, the interfering effects of stray fields at the power line frequency and its harmonics are negligible. This frequency is low enough so that measurements can be made at the second and third harmonics of the recorded frequency—1,400 cycles per second and 2,100 cycles per second. It was thought to be possible an erase head might erase the fundamental component of the recorded signal but leave harmonic components on the medium. This was found not to be the case, however. No second harmonic could be detected under any condition of measurement. A third harmonic component in the remanent signal was measurable, particularly at low values of erase current where the remanent signal was only slightly below the saturation level. The measurements indicated, however, that the major portion of energy in a remanent signal is at the recorded frequency and that measurements with the play-back system tuned to the recorded frequency are a sufficiently accurate measure of the total energy left in the medium.

Performance of Erase Heads

Data for the curves of Figure 5 were obtained by using apparatus and test methods described in the previous section. The recordings were made at 700 cycles per second and at saturation level. The play-back system was tuned to this frequency in making the remanent signal measurements. The magnetic properties of the recording wire used in these tests are typical of presently used wires. A loop of wire driven at two feet per second was used.

The heads used in these tests were handmade. Magnetic parts were made by cutting them from annealed sheet stock with a fine saw. The pieces were not annealed after cutting. While care was taken not to bend the magnetic material, it is known that even the cold working due to sawing has an adverse effect on the magnetic properties. Performance of each of the erase head types, therefore, would be somewhat better than that shown in Figure 5 if the magnetic parts were annealed after fabrication.

After the maximum erase with the turn-in-gap head, the sound analyzer was detuned slightly in both directions from the frequency of the recorded signal. No change in level was detected. This indicated that the recorded signal had been completely erased and all that remained on the wire was random noise.

Erasing action sufficient to reduce a saturated signal 40 decibels probably is satisfactory in a low-cost home recorder because (*a*) in such a recorder hum and amplifier disturbances rather than wire noise determine the background noise level which, because of economic considerations, probably will be no more than 40 decibels below the saturated signal level; (*b*) a recording made at proper level should be saturated upon signal peaks only, if at all, and signal levels below the saturation level are more easily erased than saturated signals; (*c*) remanent signals can be further reduced by running the wire through the erase head several times. It is evident from Figure 5 that the turn-in-gap head provides this degree of erase at about 0.6 of the power required by the solder-lug head and 0.4 of that required by the combination head. These lowered power requirements probably are sufficient to allow the use of a smaller oscillator tube with corresponding savings in the power supply.

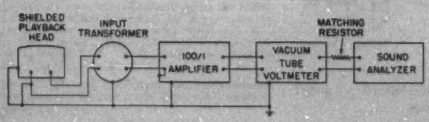
Through refinements in design in a high quality recorder, the over-all background noise level of the machine, including wire noise, may be reduced to a level 60 or more decibels below the saturated signal level. In order that they do not contribute significantly to the over-all background, remanent signals should not exceed a level 70 decibels below the saturation level in such a machine. This degree of erase 10 decibels in Figure 5 is obtained with a power of 2.6 watts in the turn-in-gap head. Neither the combination head nor the solder-lug head of Figure 2 will operate continuously at this degree of erase without overheating. In fact, in obtaining data for these heads at high current it was necessary to limit the time of application of the current. This precaution was not necessary with the turn-in-gap head. While temperature runs have not been made on these heads, the 2.6 watt input to the turn-in-gap head definitely is a safe value. This head has been operated at approximately four watts for a long period of time with no signs of damage due to overheating.

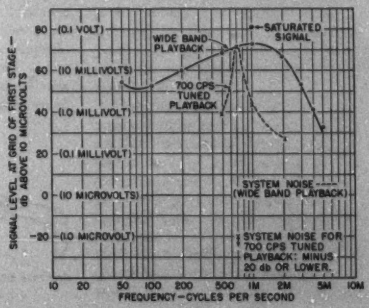
A turn-in-gap erase head has been in use in a tester for routine tests on wire samples at the Armour Research Foundation. A wide variety of wires, some considerably more difficult to erase than presently used wires, have been tested. It has been possible in all cases to completely erase saturated signals on these wires. In this tester, the condition of complete erase is determined by a listening test. The high frequency current in the erase head is increased until the remanent signal is completely masked by the random background noise.

With previous erase head designs it has been extremely difficult to restore to a wire a background noise level as low as the

Figure 4 (right). Characteristics of play-back system

Figure 3 (below). Play-back system for erase head tests





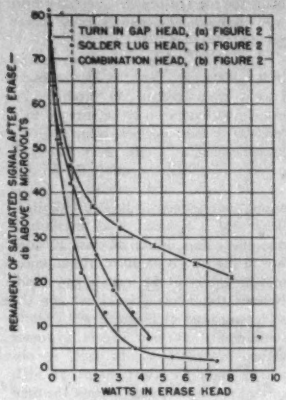


Figure 5. Performance of erase heads

level prior to the recording of any signals. With the turn-in-gap head it is possible to reduce the noise level of a virgin wire (as-drawn) by about four decibels. This lowered noise level also could be obtained after recording a saturated signal and erasing this signal. These noise measurements were made under the conditions of restricted high frequency response in the special test setup (wide band play-back curve of Figure 4). It was found that effective erasing action with this new type head does not require good magnetic contact between the wire and head groove. As the wire was gradually lifted out of the groove, no decrease in erasing action was noted until the wire reached the top of the groove.

There are two basic reasons for the improved efficiency and greater available erase power in the turn-in-gap head.

1. Major losses in an erase head are those in the ferromagnetic structure. For a given erasing field these losses are reduced in the turn-in-gap design by reduction of the head flux for a given erasing field through reduced stray fluxes, and by reduction in effective length of flux path—the minimum length flux path is the shortest distance around the gap strap rather than around the window in a conventional head.

2. Use of a constricted part, gap strap, of very short length in the secondary turn, and provision of good thermal contact between the gap strap, the head laminations, and the major part of the secondary turn allows the rapid removal of heat from regions of high loss concentration—gap strap and head laminations. These improved thermal characteristics allow the head to be excited to higher flux densities than is usually possible without excessive temperature rise.

Conclusion

The turn-in-gap erase head is more efficient than other types in that less power is required for a given effectiveness of erase.

Improved heat dissipating characteristics of the turn-in-gap head together with its improved efficiency, allow this head to supply erasing fields of sufficient strength to obliterate completely all effects of previously recorded signals on any wires that are likely to be used in magnetic recorders.

With the turn-in-gap erase head, broad band wire noise, as measured with the equipment described, can be reduced to a level lower than that of the virgin wire noise level measured under the same conditions.

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No Discussion

Western Union Teletape Facsimile

LEON G. POLLARD
NONMEMBER AIEE

IN THE APPLICATION of facsimile to the telegraph industry, development has primarily been directed toward the adaptation of such equipment to the handling of regular telegraphic traffic in the form of messages on telegraph blanks. The introduction of Western Union's "Teledeltos" recording paper some years ago provided a practical recording medium which was one of the principle things necessary to make facsimile telegraphy a success. Facsimile messages can be recorded directly on teledeltos paper made up in the form of telegraph blanks, and without any special processing or handling, these messages are ready for delivery.

A number of articles[1-4] have been published describing the equipment which has been developed by Western Union engineers and is now used on Western Union railroad and airline facsimile telegraph circuits. Some machines require the message to be manually placed in position on the drums while other machines are self-operating to the extent that it is only necessary to drop a message in a slot of the transmitter and it is automatically wrapped around a revolving drum, scanned and transmitted to the receiving instrument, where the functions of loading, recording and discharge of the message are again automatically performed.

In all machines of this class the light-beam scanning and photoelectric method of pickup is used and recording is accomplished by means of a metallic needle point on teledeltos paper. During the war civilian engineers at Camp Coles Signal Corps Laboratory made the discovery that pencil marks on teledeltos paper provided a convenient medium for conductive pickup. While optical methods of pickup in general produce excellent copy and do not require special papers, the cost of the necessary components somewhat restricts the field of application. With the disclosure that teledeltos paper can be utilized for the purpose of transmitting as well as receiving facsimile messages, an entirely new field for this system of communication has become available. It has been realized that there is a need for a simple means of transmitting written intelligence between one or more points such as are now served by oral or other local intercommunication systems. There are many instances where a recorded rather than an oral message is necessary or desirable, and there are many cases where it would be convenient to communicate to periodically unattended points where the messages would be on record for future handling.

With such possibilities in mind, Western Union has developed facsimile equipment to fulfill these requirements. The machine used for this purpose is a transceiver which is called the "Teletape." It has been so named because teledeltos paper in the form of tape provides a convenient and practical way of accomplishing the dual purpose of transmitting and receiving. These teletape transceivers, which are of small dimensions, are located at the terminals of a 2-wire circuit, and will operate satisfactorily when these terminals are separated by a distance up to several miles. Messages written directly on teledeltos tape are scanned, transmitted over the line and reproduced in facsimile form on the tape at the opposite end. Transmission can be carried out in either direction as the machines automatically function as transmitters or receivers.

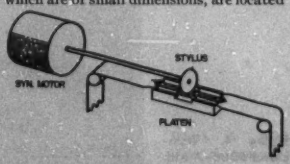

Figure 1. Principle of the teletape scanning system

As this is the first time that teledeltos paper has been used in this capacity, several problems encountered in the development of this system may be of interest. When using teledeltos for transmitting purposes, the intelligence is impressed on the surface of the paper either by means of a soft lead pencil or from a specially prepared carbon paper. In the teletape system a pencil is used. As the message is written, the film on the surface of the paper is ruptured and graphite from the pencil forms a low resistance path through the paper. The paper which has a conductive backing is supported by a metallic platen and scanning is accomplished by means of a fine metal stylus which is progressed across the surface. As the point of the stylus passes over the paper, the resistance between the stylus and the platen is reduced by several thousand ohms at the points where the film of the paper has been broken and graphite deposited. This resistance change is utilized as the actuating impulse in this method of pickup. For satisfactory operation of this conductive pickup system it is necessary to use a stylus having a small area of contact and yet of such design that the surface film of the paper will not be damaged during scanning.

After the investigation and trial of a number of standard scanning systems, a simplified method has been developed for the teletape in which several of the difficulties encountered in other systems have been overcome. In all facsimile systems there are two fundamental requirements which must be fulfilled. First, the facsimile receiver must run in synchronism with the transmitter, and second, some method must be employed

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LEON G. POLLARD is assistant electronics research engineer with Western Union Telegraph Company, Watermill, N. Y.

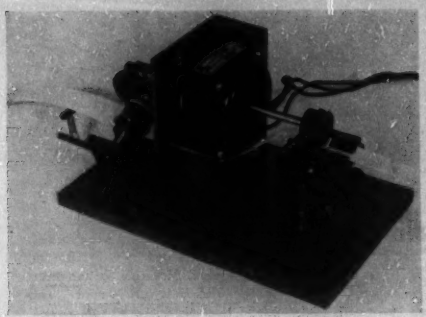


Figure 2. Simple model for the rotary scanning of narrow tape

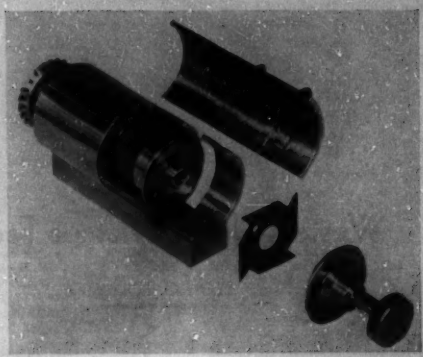


Figure 3. Exploded view of teletape scanning head

for phasing the machines to insure proper registration of the received message. When both ends of the circuit are powered from the same 60-cycle system, synchronism is automatically maintained. However, phasing is still required and is usually accomplished by means of a phasing pulse from one end of the circuit, which through a system of relays and commutators, orients the machine at the opposite end.

Figure 1 illustrates the principle of the teletape scanning system. Teledeltos tape having a width of 3/4-inch is directed through a concave guide and passes by a rotating four-pointed stylus which brushes over the surface of the paper. This stylus is mounted on the shaft of a small synchronous motor running at 1,800 rpm. The concave guide has a radius of exactly 1/2-inch about the axis of the stylus shaft and the diameter across the tips of the stylus is slightly in excess of one inch. A narrow recess is machined in the concave guide directly behind the line of contact of the stylus with the tape. The radius of the stylus being somewhat greater than that of the concave guide results in the tape being slightly depressed into this recess as it passes by the stylus during scanning. This insures a positive rubbing contact of the stylus tips with the surface of the tape. Furthermore, as the tape passes over the recess in the guide, the edges are flattened just sufficiently to allow the tips of the stylus to miss contact at the extreme edge and prevent cutting the paper.

Figure 2 shows a simple model which was constructed to determine the possibilities of this system of scanning and recording. The same stylus is used for conductively picking up a message which has been written on the tape or for electrically recording a message fed to it from another similar machine.

It will be observed that with a 4-pointed stylus, one point for each pole of the 1,800 rpm motor, phasing of the transmitter and receiver each time they are started is no longer necessary. Initially, it is necessary that the multiple pointed stylus on each machine be so adjusted that the tips are in the same relative position with respect to the motor poles, but thereafter, providing of course that both the transmitter and receiver are powered from the same 60-cycle system, no phasing is required. Initial phasing is easily accomplished by marking a straight line at a definite position on the tape and transmitting this sample to the receiver. The receiver is then adjusted to bring the recorded line to the same position. It will also be observed that the 90-degree displacement of the stylus tips on a slightly greater than 1-inch diameter is in excess of the width of the tape by just a sufficient amount to prevent simultaneous contact of two tips with the tape. This results in approximately 95 per cent of the line time being utilized for message transmission.

Figure 3 shows an exploded view of the scanning head used in the teletape machine. A polished cylindrical housing serves as a platen against which the tape is firmly pressed by a concave shoe as it is drawn through the machine. The stylus is a thin 4-pointed metal wafer so constructed that the tips are accurately spaced by 90 degrees and of equal radius. The narrow recess in the concave shoe aligns with a cutout portion of the cylindrical housing and the stylus engages the tape at the center of this opening. The cylindrical housing also contains two precision type ball bearings supporting the rotating stylus shaft. On one end of this shaft is a small miter gear, which when assembled in the machine, engages a similar gear mounted directly on the shaft of the synchronous motor. The other end of this shaft carries an insulated stylus mounting provided with a knurled thumbscrew to hold the stylus rigidly at right angles to the shaft. On the surface of this stylus mounting is a small pin which engages a hole punched in the stylus. This orients each replaced stylus in a definite relationship with respect to the motor poles, which as mentioned before, is the only requirement necessary to take care of the phasing problem.

Figure 4 shows a type *5006-A* transceiver and its associated power and con-

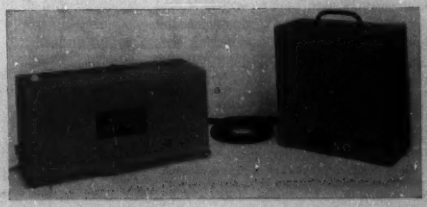


Figure 4. Transceiver 5006-A with power control unit 5046-A

Figure 5. Teletape transceiver with tape-guide cover partly opened

Figure 7. Inside view of transceiver showing tape pull-off mechanism and stop switch

trol unit type *5046-A*. These two units connected together through a 12-conductor flexible cable comprise one terminal of a teletape circuit. The transceiver unit consists of the scanning mechanism, a tape reel holding a $5^1/_2$ inch roll of teledeltos tape, and a pull motor for feeding the tape through the machine. The power and control unit contains the transmitting and receiving amplifiers and the necessary relays and power supply equipment. When installed in an office or other terminal position, the transceiver unit is placed in a convenient location for the user and the power control unit is located in a more or less out of the way place where it is accessible only for turning on and off once a day.

Figure 5 shows the transceiver only. It will be noticed that in the top of the machine is a long narrow opening. In this opening and flush with the top of the case is a writing platform over which the teledeltos tape from the storage reel is passed. A hinged cover with a short projecting handle covers this tape guide. In this particular model of the teletape the opening and closing of this cover actuates switches which in turn operate the circuit control relays. When not in use the covers should always be left in a closed position to maintain a normal stand-by condition. To illustrate the operation of the machines, let us assume that station *A* desires to transmit a message to station *B*. The operator at station *A* first opens the cover over the tape guide. This is necessary to expose the tape on which the message is to be written, but as the cover is opened, a circuit is made which lights indicating lamps at both stations *A* and *B*. At the same time cover locks at both stations are energized and the cover over the tape guide at station *B* is now locked in a closed position until the completion of the transmission. This prevents the operator at station *B* from attempting a simultaneous transmission. The operation of opening the cover at station *A* also energizes relays at both ends of the circuit so that station *A* will function as a transmitter and station *B* as a receiver for this particular transmission. After the operator at station *A* has written his message on the tape, he closes the cover. This operation of closing the cover starts the motors and scanning mechanisms at both terminals and transmission is accomplished. At the end of the message both machines are automatically stopped and restored to a stand-by condition ready for the initiation of another transmission from either end. When the cover at station *A* was closed it also was latched to prevent further opening until this message had been completed.

Should station *B* desire to transmit to station *A*, the same functions are carried out in a reverse direction. Opening the cover at either end of the circuit preempts the circuit for transmission from that end and at the same time prevents the operator at the opposite end from attempting to initiate a transmission.

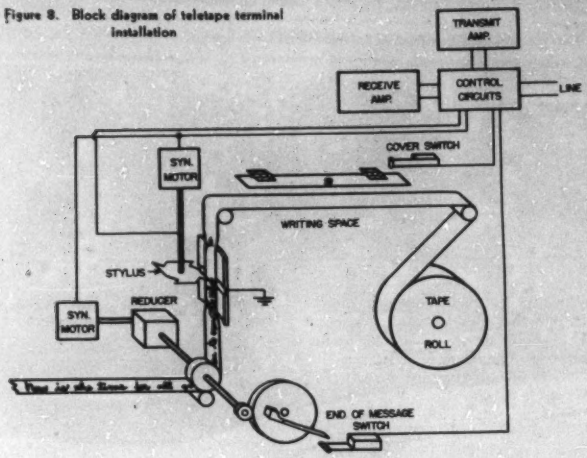
Figure 6 is a view of the interior of the machine. It shows the relationship of the scanning head, tape reel and writing platform and the tape pulling gear with its associated motor. The scanning head motor located behind the supporting bracket drives the scanner through a miter gear. It is mounted on a circular plate capable of rotation about the center of the motor shaft by which means the relationship of the motor to the stylus may be conveniently changed for initial phasing purposes. It will be observed that as the tape leaves the writing guide it passes over a roller and then assumes the concave form in passing through the scanning head. It thence passes over a second roller and through the draw-off mechanism.

Figure 7 shows the reverse side of the machine. The tape draw-off motor as well as the scanning motor is of the synchronous type. Connected through a reduction mechanism from this motor is a low-speed gear which makes one complete revolution during the transmission of a single message. A lever on this gear wheel actuates a trip switch just as the end of the written message is exposed out of the side of the machine. The motors of both machines are thereby stopped at the same instant at the end of each transmission.

As mentioned before, a-c power to the machines can be turned on at the beginning of the day and turned off at night. This insures that the amplifiers are in a warmed-up condition and ready for instantaneous operation. In order that the operator at one end of the circuit can be

Figure 6. Inside view of transceiver showing scanning mechanism

Figure 8. Block diagram of teletape terminal installation



assured that the equipment at the opposite end is in an operating condition and ready to receive messages, a series line switch is incorporated with the power switch. The initiating circuit which is energized when either cover is opened, is a series circuit through both line switches. If, when the operator at one end lifts the cover on his machine, the signal lamp at his end does not light, he knows that power has not been applied to the distant end and no message can be transmitted.

Figure 8 is a functional diagram showing a teletape terminal installation. In this particular model of the teletape, the length of exposed tape in the tape guide on which a message may be written is 11 inches. The transmission time for each message is 18 seconds. The tape reel holds a 600-foot roll of tape which is sufficient for a total of about 325 messages. Development work is now in progress to determine the most satisfactory material from which to fabricate the wafer type of styli for this machine and also the possible advantages of other forms of multi-pointed styli. The styli used at present are hardened steel and each stylus has a life of between 300 and 400 messages. A worn stylus may be easily replaced.

The teletape terminal electronic equipment consists of two amplifiers, one each for transmitting and recording. The conversion of the written message to electrical impulses is somewhat unique in that the circuit from the stylus through the teledeltos tape to ground forms one arm of a Wheatstone bridge, the output of which is fed to the transmitting amplifier. The signal frequency applied to the bridge is generated by a 5,000-cycle local oscillator. This comparatively high frequency is dictated by the high scanning speed of 94-inches per second employed in this system. If the bridge is balanced for background resistance, positive transmission results, because a pencil mark on the tape unbalances the bridge with consequent signal to amplifier. Conversely, if the bridge is balanced for a solid mark on the tape, negative transmission results. Practice limits line input levels to about zero decibel above one milliwatt and one stage of amplification following the bridge is more than adequate to fulfill this requirement.

The recording amplifier consists of a voltage amplifier triode and a power amplifier pentode which has a high impedance output to the recording stylus. Adequate gain is provided so that this equipment may be operated over lines with as much as 30-decibel loss.

Figure 9 is a view of the interior of the power control unit. On the lower chassis are the signal control relays and the power supply while the upper chassis carries both the transmitting and receiving amplifiers.

Figure 9. Interior view of power control unit

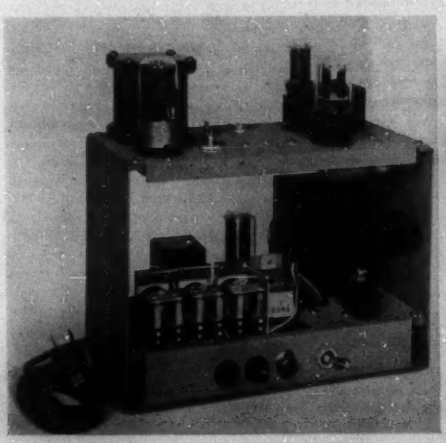


Figure 10. Typical installation of a teletape machine

From this description one may see why the teletape, utilizing facsimile as a means of transmission, is finding applications to intercommunication systems. It provides a means for the speedy transfer of written intelligence from one point to another. It does not require the immediate attention of the recipient and it can be handled by anyone without the need for special training in its operation. There are a great many instances where such a system is desirable. A specific application is the linking of a teller in a branch bank with the bookkeeping department, located in another building. An experimental installation of this type is giving satisfactory service in one of the New York banks. The teller can place a request for certain information via the teletape, proceed with his work and receive an answer with a minimum of attention. Figure 10 illustrates an installation of this type.

Another similar application is the connection between a ticket clerk and the reservation bureau such as found in railroad stations and airline offices. The ticket clerk can ask for a reservation for one customer and continue with succeeding customers while the necessary information is being obtained and recorded. Many other applications such as dispatching systems and quotation services are becoming apparent. At present, teletape equipment is undergoing tests with a view to proceeding with its manufacture and distribution as soon as all the apparent operational requirements have been fulfilled. It is hoped that the teletape with its comparatively simple method of operation will in a small measure contribute to the advancement of the art of facsimile communication.

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No Discussion

Survey of Lightning Protective Equipment for Rotating A-C Machines

AIEE LIGHTNING PROTECTIVE DEVICES SUBCOMMITTEE*

THIS SURVEY was initiated to determine the present practices used to protect rotating a-c machines against lightning.

Method Employed

Permission was obtained from the Edison Electric Institute to circularize the members of the Electrical Equipment Committee with a short questionnaire on this subject. This questionnaire is shown as Figure 1.

Coverage of the Industry

The members of the Electrical Equipment Committee are well dispersed throughout the country.

In all, questionnaires covering 61 companies were sent out. From the McGraw Central Station Directory for 1945 the companies solicited that rated their generating capacities in megawatts, produced a total of 12,465 megawatts, and those reporting in megavolt-amperes produced a total of 11,030 megavolt-amperes. If a nameplate rating of 0.9 power factor is assumed for those reporting in megavolt-amperes the sample adds up to 22,390 megawatts, nearly 45 per cent of the installed generating capacity in the country.

Since the information was obtained from the foregoing source, no plants operating under government agencies are included. It happens further that since no members of the group solicited were from the companies operating in New York City and Chicago these two large centers are not included in the results.

Of the 61 companies solicited, replies were received covering 54 of the companies.

Analysis of Questionnaire Replies

Table I shows the specific answers to the questions in detail. However, certain comments can be made with respect to the practices and ideas expressed in the tabulated replies:

1. Of the companies answering, 30 definitely favor protection and have installed it; 16 do not have the equipment installed, except in special cases; 8 companies have had such equipment recommended to them for installation.

2. The minimum size of machine protected varies greatly. In some cases this happens to be determined by the possible exposure, and the minimum size is determined by this, rather than the establishing of a definite size below which protection is not applied This also affects the number of machines protected.

3. Machine failures due to lightning before the protective equipment was installed vary

American Institute of Electrical Engineers Lightning Protective Devices Subcommittee

Survey of Lightning Protective Equipment Used for Rotating A-C Machines

1. Does your company use lightning protective equipment for rotating a-c machines? Yes..... No.....
2. What size of machines is it your practice to protect? .....Kva and larger.
3. How many machines of this kva rating and larger do you have on your system? .....
4. To how many of these machines have you applied lightning protective equipment? .....
5. How many of these machine failures did you have from lightning before lightning protective equipment was applied? .....
6. How many of these machine failures have you experienced from lightning where lightning protective equipment was applied? .....
7. What lightning protective equipment do you use?
   (a) Lightning arresters only? Yes..... No.....
   (b) Capacitors only? Yes..... No.....
   (c) Combination of Arrester & Capacitor? Yes..... No.....
8. Is it your practice to use arresters of reduced rating where the neutral of the machine is solidly grounded ($X_0/X_1 = 3$ or less)? Yes..... No.....
9. Is it your practice to apply lightning protective equipment to machines?
   (a) Directly connected to overhead lines? Yes..... No.....
   (b) Connected through transformers? Yes..... No.....
10. What is the attitude of your company toward application of lightning protective equipment of rotating a-c machines:
   (a) Has it been worth while? Yes..... No.....
   (b) Do you expect to apply more of this equipment in the future? Yes..... No.....
   (c) Give any comments you may have on this subject, particularly your estimate of the average number of thunderstorm days per year on your system.

COMPANY.....

BY WHOM.....

Figure 1

Paper 48-78, recommended by the AIEE protective devices committee and approved by the AIEE technical program committee for presentation at the AIEE winter general meeting, Pittsburgh, Pa., January 26-30, 1948. Manuscript submitted December 1, 1947; made available for printing December 8, 1947.

*Survey made and report prepared by T. H. Mawson, chairman AIEE working group on survey of lightning protective equipment used on a-c rotating machines. No other members of working group.

Personnel of AIEE lightning protective devices committee: F. M. Defandorf, R. H. Earle, Milan Getting, J. W. Gross, E. H. Grosser, H. W. Hartzell, T. H. Mawson, J. R. McFarlin, W. A. McMorris, A. M. Opsahl, W. E. Rushlow, A. H. Schirmer, J. M. Towner, E. R. Whitehead, E. H. Yonkers, and H. R. Stewart, Chairman.

ble I. Answers to Questionnaire on Lightning Protection for Rotating A-C Machines

Company Reporting	1*	2	3	4	5	6	7 (a)	7 (b)	7 (c)	8
1	No				?	None			X	
2	Yes	3,125 kva and larger	71	37	Few old hydro units	None			X	Min. possible
3		12,500 kva and larger	3	3	None	None		X	X	Yes
4	Yes	500 kva and larger	12	1	Negligible	None			X	Min. possible
5	No			1	?	None			X	
6	Yes	300 kva and larger	40	Majority	Probably 2	None			X	Yes, if grounded solidly
7	Yes	All sizes	30	30	6 ±	3 (with only partial protection)	15		15	Yes
8	Yes	29,000 kva and larger	4	4	0	0	X			Yes
9	No	None		None	None					
10	Yes	50,000 kva and larger	7	2	None	None			X	None solidly grounded
11	Yes	30,000 kva and larger	5	2	None	None			X	No
12	Yes	500 kva and larger	33	26	10	None			X	Yes
13	(Included in Company Number 30)									
14	Not generally			2	None	None	X		1 motor	Yes
15	Yes	375 kva and larger	81	15	Some on small units	None of importance	X		X	Yes
16		35,700 kva and larger	2	1	None	None			X	
17	Yes	Condensers and small hydro generators	22	22	Indeterminate	Indeterminate	X		X	No
18	Yes	See Note (a)	83	21	6	0			X	
19	Yes	200 kva and larger	81	64	12 per season	4 per season	20 per cent		80 per cent	Yes
20	Yes	25,000 kva and larger	1	1	None	None			X	Special lightning arrester with capacitor
21	No				None					
22	No				None					
23		16,500 kva		1	None	None			X	Yes
24	Yes	500 kva and larger	47	9	Negligible	None			X	Yes
25		25,000 kva and larger		1	None	None			X	Yes
26		25,000 kva and larger	2	2	None	None			X	
27	Yes	5,000 kva and larger	4	4	1	None			X	Special lightning arrester with capacitor
28		56,000 kva and larger	1	0	None					
29	(Included in company number 30)									
30	Yes	All	170	100	25 in 20 years	3 in 10 years	85		15	Yes
31		30,000 kva and larger	2	0	None					
32	Yes	300 kva and larger	35	21	5	None			X	Yes
33	Yes	1,000 kva and larger	55	6	None	None			X	
34	Yes	5,000 kva and larger							X	No
35	Yes	20,000 kva and larger	9	9	0	None	X		X	
36	Yes	500 kva and larger	44	7	Negligible	None			X	Yes
37	No									
38	Yes	No base value	100	16	4	1	X		X	Yes
39	Yes	43,750 kva and larger	1	1	None	None	X			Yes
40	(Included in company number 44)									
41	No				None					
42	Yes	25,000 kva and larger	25	12	None	None	X		X	Yes
43	No									
44	No									
45	No		13	None	None					
46	No	None		None						
47	Yes	9,375 kva and larger	7	7	2	None	X		X	Yes
48	No			2	None	None			X	No
49		18,750 kva and larger		None	None					
50	No									
51	Yes	1,000 kva and larger	75	Practically all	25 ±	None	75 per cent	10 per cent	15 per cent	No
52	Yes	300 kva and larger	35	11	3	None			X	Yes
53	Yes	300 kva and larger	18	4	12	1	X		X	Yes
54	No									Yes

Note: * Column headings are question numbers.

Table 1. Answers to Questionnaire on Lightning Protection for Rotating A-C Machines (Continued)

Company Reporting	9 (a)	9 (b)	10 (a)	10 (b)	10 (c)	Remarks
1				Yes	70	New installations have been protected, practice not general on system
2	Yes	Some	?	Probably		Tests are in progress to determine proper functioning
3		Yes		Yes		
4	Yes	Some	?	Probably		Tests are in progress to determine proper functioning
5						Special case of 1,000 kw M-G set. New coils installed and no failures since, although protection was installed six months before coils were replaced
6	Yes	Yes	Probably	Yes		Some variation between divisions based upon experience
7	Yes	Yes	Yes	Yes		Practice will be extended as soon as possible
8	Yes		Yes	Yes		Smaller machines (375 to 15, 625 kva) connected to buses with lightning arrester protection on the bus
9	No	No	No	No	60	
10	Yes	No	Yes	Yes	41	
11		Yes		Yes	40	
12	Yes	In some cases	Yes	Yes		
13						Included in company number 30
14	Special cases only		No	Special cases only	40	Present special installations to be removed. Rotating machines generally connected to underground system
15	Yes	Yes	Yes	Yes		60 Machines connected to buses protected by arresters. Arresters and capacitors used in some cases where windings are multiturn coils
16		Yes		Yes		
17	Yes	Yes	Yes	Yes	33	Capacitor and lightning arrester combination used on hydro units to protect old, damp insulation
18	Yes	No	Yes	Yes	40	Protective equipment has occasionally failed. Small machines in mining areas protected for safety reasons. Rotating machines generally connected to system through transformations or cables
19	Yes	Yes	Yes	Yes	80	
20	Yes	Yes	Yes	Yes	70	
21	No	No			50–75	New machines will be considered individually. No failures from lightning in 10 years
22				No	29	Past experience has not shown necessity for protection
23		Yes		Yes		Only one 16,500 kva unit protected
24	Yes	Some	?	Probably		Minimum arrester used. Tests in progress to determine proper functioning of protective equipment
25		Yes		Yes		
26		Yes		Yes		
27	Yes	Yes	Yes	Yes	70	
28				Yes		
29						Included in company number 30
30	Yes	No	Yes	Yes		
31				Yes		See Note (b)
32	Yes	No	Yes	Yes	26	
33	Yes	Yes	Probably	Yes		Would use reduced rating lightning arresters if solidly grounded. Most machines are connected to underground circuits
34	Yes	Yes		Yes		Recently adopted this practice. Future experience will dictate extensions
35	Yes	Yes	Yes	Yes		An additional 17 machines are connected to buses protected by lightning arresters. Capacitors used on multiturn coil machines only
36	Yes	Some	?	Probably		Minimum lightning arresters used. Tests in progress to determine proper functioning of protective equipment
37						Not sufficient exposure to require protection
38	Yes	Yes	Yes	Yes	30	Protection applied on basis of experience, exposure and size of unit
39		Yes	Yes	Yes		All units installed since 1938 are equipped and tests are in progress on others
40						Included in company number 44
41						Not sufficient exposure to require protection. Overhead lines carry arresters where they enter the station
42	No	Yes	?		25–30	Will be applied in future when recommended by manufacturer
43						Usual substation lightning arresters considered sufficient
44		No			45	High side arresters and ground wires considered sufficient
45	No	No				
46			No	No		Units operate ungrounded. Failure of small units connected to overhead lines expected
47	Yes	Yes	Yes	Yes		Capacitors used on multiturn coil machines
48	Yes	No		Some	50–60	Future installations to be made when justified
49				Yes		
50						No large rotating equipment
51	Yes	Yes	Yes	Yes	45	
52	Yes	No	Yes	No	10	All units directly connected to overhead lines are protected
53	Yes	No	Yes	Yes		Directly connected to overhead only
54	No	No	No	No	43	Bus or line arresters considered sufficient

a. No arbitrary rating. Protection determined by exposure, importance of load served, cost of repairs.

b. Lightning arresters always used on machines where connected to overhead circuits unless these circuits are in substations remote from the machine. Capacitors have been added to the lightning arresters where troubles have occurred. Protection of synchronous condensers (7,500 kva and up) has consisted of capacitors and this will be increased where overhead lines are involved.

considerably; however, the use of protective devices has, in general, reduced the number of failures.

4. The majority of the companies favored the use of a lightning arrester and capacitor. However, dependent upon conditions, the practice of using one method only does not generally hold. This summarizes as follows:

(a) Using lightning arrester only	3 companies
(b) Using capacitor only	0 companies
(c) Using lightning arrester and capacitor	23 companies
Using (a) or (b)	1 company
Using (a) or (c)	13 companies
Using (b) or (c)	1 company
Using (a) or (b) or (c)	1 company

5. The practice of using lightning arresters of reduced rating where the machine neutral is solidly grounded is followed by 21 companies. Six companies do not do this and others did not answer the question specifically.

6. Basically the question of applying lightning protection should be determined by possible exposure. Consequently, the idea of protecting machines which are directly connected to overhead lines seems to be generally prevalent. However, there is some divergence of opinion where these lines are connected to buses or where connection is made through cables to transformers which feed a bus that is protected by lightning arresters. For equipment directly connected to overhead lines, 27 companies installed protection, 9 did not. For equipment connected through transformers, 23 companies installed protection, 14 companies did not, and 6 companies installed protection in certain locations only. There were 43 companies that replied to this question and of these, included in the foregoing summary, 13 companies installed protection in either case and 4 companies in neither case.

7. In answer to the question as to whether this practice has been worthwhile, 32 companies answered the question with 21 affirmative answers, 4 negative, 2 probably, and 5 questionable until further tests were made, which tests are now in progress.

8. As to whether the companies expected to apply more of this equipment in the future, 45 companies replied. A marked tendency toward installing protection was evidenced by 33 companies definitely expecting to continue this work, 4 more would probably do so, 3 where it appeared to be justified, and only 5 replying in the negative.

9. The isokeraunic levels vary from about 80 thunderstorm days per year to the very low levels found on the Pacific Coast. There seems to be little correlation between these values and the extent of protection for rotating machines of the type mentioned herein.

Conclusions

At the present time past experience seems to be one of the major criteria for installing lightning protection on rotating a-c machines. Exposure conditions rather than size is a determining factor. The general trend seems to favor more of such protection and to lean toward the use of arresters and capacitors in combination. The results of tests now in progress should do much to clarify the situation in doubtful cases.

Discussion

E. H. Grosser, (Commonwealth Edison Company, Chicago, Ill.): This paper is of considerable interest in showing, from an analysis of the operating records of a large number of utilities, a definite justification for the lightning arrester protection of rotating machines.

The record presented shows a marked and consistent reduction in lightning failures where protective measures have been applied. Of 37 companies reporting the use of lightning protection on rotating a-c machines, 20 had no failures, or practically none, either before or after installing protective equipment, and two companies gave no report on failures. The remaining 15 companies, without exception, reported definite reductions in lightning failures after lightning arrester protection was installed. For eight companies, a complete elimination of failures resulted and for seven companies, assuming that the data given for before and after conditions covered comparable periods of service, there was a reduction in lightning failures of from 50 to 92 per cent of the previous rates. For all 15 companies, the average reduction amounted to 87 per cent of the failures before installing lightning protection.

Since Chicago practice and experience were not covered in the paper, it might be worthwhile to mention them here. Our generation is at either 12 or 22 kv and outgoing lines at generated voltage are, in all cases, underground cable lines. At transmission voltages (33, 66, 132, and 220 kv) the generators are separated from transmission lines by transformers. Lightning arresters are installed at the station end of all overhead transmission lines, except those with long cables between the station and the overhead line. With the single exception of a 75,000-kva synchronous capacitor connected to a 220-kv overhead line through a transformer, none of our rotating a-c machines are provided with lightning protection. In operation, there have been no generator failures attributable to lightning.

Edward Beck (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The report on lightning protective equipment for rotating a-c machines represents an appreciable cross section of the practices and experiences of the utilities. The report states that no data from Public Power Projects are included, whose experience would be of additional interest.

It should be recognized that there is a large and still growing volume of machine protection installed by industries such as steel mills, mining companies, pipe line pumping stations, and the like. The circuits are often exposed to severe lightning and sometimes handicapped by difficulties in obtaining low-resistance grounds. Protection is frequently applied to motors of relatively small horsepower. The economies in maintenance and loss of service have apparently been well established. A similar survey among these industrial users would be of interest to the electrical industry as a whole and the industrials in particular. We suggest that such a survey be planned for the future.

In Table I, under "Remarks," mention is made several times that tests are in progress to determine proper functioning. The nature of the tests and the instruments used to accumulate data would be of interest.

S. B. Howard (General Electric Company, Pittsfield, Mass.): The experience data on the use of lightning protective equipment for rotating a-c machines which Mawson's group has so ably brought to the attention of the industry, should do much to remove any doubt as to the value of such equipment in reducing machine failures attributable to lightning.

The data contained in columns 5 and 6 of Table I are particularly illuminating. Out of 15 companies reporting lightning failures prior to installation of protective equipment, 9 reported no failures where the equipment was applied, 5 reported from 50 to 90 per cent less failures and 1 reported "no failures of importance."

It should be most gratifying to all advocates of lightning protective equipment for rotating machines to note the justification of their recommendations by such excellent service experience.

Expulsion-Type Lightning Arresters' Impulse Spark-Over Volt–Time Characteristics

AN AIEE COMMITTEE REPORT

THIS REPORT presents the impulse spark-over volt–time characteristics of distribution lightning arresters of the expulsion type, as they are currently available. These arresters, rated from 3 to 15 kv inclusive, are covered by AIEE Standard 47 issued for trial use in December 1945. In that Standard several performance characteristics are enumerated, and the methods of determining the characteristics are described. Although all of the performance data described in the Standard are not yet available, it is thought desirable to present to the industry the available information on the impulse spark-over characteristics that may be expected, especially since in this type of lightning arrester the impulse spark-over characteristic is the most significant protective one and since Standard 47 is proposed for trial use only.

It is expected that at a later date, when additional information is available and a standard has been adopted, a subsequent report will be issued to include performance data on all the characteristics enumerated in the Standard.

The data in this report have been compiled from information supplied by seven manufacturers. The data pertain to lightning arresters as now manufactured and do not necessarily apply to older types. The present report covers only those lightning arresters available as separate pieces of equipment, as distinguished from those supplied as integral parts of other apparatus, such as self-protecting distribution transformers.

The average curve for each rating is derived from the manufacturers' average impulse spark-over volt–time characteristic of the complete lightning arrester including any integral or separate air gap. Tests were made using a 1.5x40-microsecond wave of the polarity giving the highest spark-over voltage. The front of wave spark-over at 100 kv per microsecond per 12 kv of rating is indicated by a dotted line to the curve for each rating. Each manufacturer also specified the tolerance applicable to his average curve. The characteristics for all manufacturers including tolerances fall within the tabulated maximum and minimum values at critical spark-over. Other points on the curve have a proportionate range from maximum to minimum. For the industry, the maximum deviations from the average curve are plus 58 and minus 49 per cent, but the tolerance permitted by any one manufacturer does not exceed plus or minus 25 per cent on his average curve.

The data in this report apply to the several available types of expulsion lightning arresters. To make the proper application of one of these, its impulse spark-over characteristic should be compared with the impulse withstand voltage strength of the insulation to be protected to insure that there is a satisfactory margin of protection.

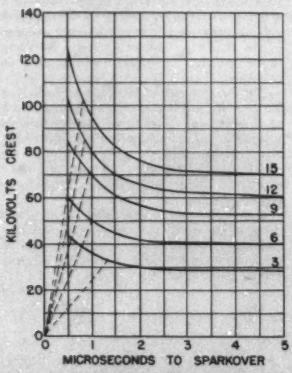

Figure 1. Average impulse spark-over volt–time characteristic of 3-to-15-kv expulsion lightning arresters

Dotted lines indicate the standard front of wave test

Table I. Expulsion-Type Lightning Arresters' Critical Spark-Over

1.5x40-Microsecond Wave

Lightning Arrester Rating	Kv—Crest		
	Minimum	Average	Maximum
3	19	29	40
6	23	41	65
9	29	53	79
12	36	61	71
15	40	70	87

Paper 48-79, recommended by the AIEE protective devices committee and approved by the AIEE technical program committee for presentation at the AIEE winter general meeting, Pittsburgh, Pa., January 26-30, 1948. Manuscript submitted December 1, 1947; made available for printing December 8, 1947.

This report was prepared by a working group of the AIEE lightning protective devices subcommittee of the protective devices committee (power group), under the sponsorship of A. M. Opsahl (M'41).

Personnel of working group of the AIEE lightning protective devices subcommittee of the AIEE protective devices committee (power group): H. R. Stewart, *chairman*, F. M. Defandorf, R. H. Earle, Milan Getting, J. W. Gross, E. H. Grosser, H. W. Hartzell, T. H. Mawson, J. R. McFarlin, W. A. McMorris, A. M. Opsahl, W. E. Rushlow, A. H. Schirmer, J. M. Towner, E. R. Whitehead, E. H. Yonkers.

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3. LINE-TYPE LIGHTNING-ARRESTER PERFORMANCE CHARACTERISTICS, AIEE Committee Report. AIEE TRANSACTIONS, volume 60, 1941, November section, pages 982–3.

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No Discussion

Underexcited Operation of Turbogenerators

C. G. ADAMS
ASSOCIATE AIEE

J. B. McCLURE
MEMBER AIEE

OPERATION of turbogenerators at high-power factors and even extending into the leading region is increasing. This trend is due in some cases to the location of large steam-electric stations remote from the load and the transmission of their outputs at high voltage. In other cases, it is due to the application of corrective reactive kilovolt-amperes in the subtransmission and distribution areas of the system, or the use of high-voltage cable in the transmission system. These conditions are a natural result of system growth and economic pressure to more effectively use the generation and transmission equipment in the supply of electrical energy.

This paper reviews underexcited operation of turbogenerators from the standpoint of limits imposed by system stability and the application of lower excitation limits on voltage regulators. Results are shown for a typical turbogenerator operating on an average system. A method for readily estimating the stability limits for other machine and system characteristics is included in the Appendix.

It is hoped that in this way reassurance will be given to those who find high-power factor operation of generators desirable in planning their normal system expansions.

Conclusions

1. Modern turbogenerators manufactured by the company with which the writers are associated may be operated successfully in the range of 60 per cent underexcited reactive kilovolt-amperes at zero kilowatt load to 30 per cent underexcited reactive kilovolt-amperes at rated generator kilovolt-amperes. Sufficient leading reactive capacity is thereby made available to supply most system demands.

2. For most practical system conditions, operation as indicated before retains adequate steady-state stability margins.

3. Voltage regulator control with an automatic lower limit rationally permits a greater utilization of leading reactive capability than does manual control of excitation.

Simplified Analysis of a Typical System

Figure 1 shows a one-line diagram of the generator or station, under consideration, supplying load to its system. Figure 2 shows the general relations between the system, machine terminal, and machine excitation voltages. Their specific relation for various conditions will be discussed later. The following conditions have been assumed.

1. The generator is rated 0.8 power factor, 0.9 short-circuit ratio (SCR). Maximum turbine output equals rated generator kilovolt-amperes which is 1.0 per unit.

2. The equivalent synchronous reactance of the generator is equal to the reciprocal of short-circuit ratio.

3. The system is represented by an equivalent reactance X_s.

4. Resistance in the circuit is neglected.

5. Since most turbogenerators have a cylindrical rotor, pullout power is assumed to occur when there are 90 degrees between the generator excitation voltage and the equivalent system voltage.

These assumptions simplify the determination of operating characteristics while not greatly affecting the accuracy of the results over a wide range of system conditions.

Effect of Generator Saturation

Generator saturation increases the steady-state pullout power over that which would exist if there were no saturation. An equivalent synchronous reactance equal to the reciprocal of the short-circuit ratio has been assumed in order to approximate the saturation effect. Results, when using this approximation, have been found to closely agree with those obtained by other more detailed methods described in reference 1, particularly for the condition of pullout at low excitation.

Representation of System by an Equivalent Reactance

Representation of a system by an equivalent reactance is fairly well known. It is simple and produces practical results. Based on generator rating, a stiff system may have an equivalent reactance in the order of 15 per cent. Systems involving long power transmission may approach a value of 40 per cent.

A fairly good approximation to the equivalent system reactance may be obtained in the following manner. In most cases the short circuit kilovolt-amperes for circuit breaker application is known at the generator bus. Reduce this kilovolt-amperes by the amount contributed by the generator in question. Divide twice the rated generator kilovolt-amperes by the reduced short circuit kilovolt-amperes. The result is approximately the system equivalent reactance in per unit. A more accurate method is to set up the system on a calculating board and insert a synchronous reactance of all generators equal to the reciprocal of their short circuit ratios. Find the reactance looking into the system at the entry point of the generator being studied. The latter method is more complicated but more

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C. G. Adams and J. B. McClure are both with the central station engineering division of the General Electric Company, Schenectady, N. Y.

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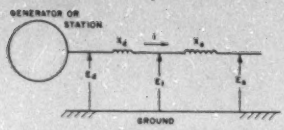


Figure 1. One line diagram of system

accurate. Since steady-state pullout powers are not affected greatly by small variations in equivalent system reactance, not much effort can be justified in obtaining an extremely accurate system representation.

Generator Operating Limits

Figure 3 is a plot of reactive kilovolt-amperes and kilowatt output for the assumed generator operating at its rated terminal voltage. Positive reactive kilovolt-amperes is supplied by an overexcited generator. The circle *ABCE* is the well known limit due to rated line armature current. The curve *IB* is the limit due to rated field current. Area *CGKL* is suggested for the setting of the operating range of lower excitation limits being applied on modern voltage regulators. Stability limits for two values of equivalent system reactance are shown by curves *JF* and *DF*. (See Appendix I for curve derivation.)

The tendency for heating in the end structure of the armatures of turbogenerators with rated armature current and low field current was recognized as long ago as 1920. Up to that time, all machines were built with magnetic materials in the end structures, which contributed to heating in these portions of the machine. The operation in the underexcited region of machines built prior to about 1925 should be approached advisedly and preferably should be checked by temperature measurement under actual operating conditions. As a result of design progress, experience and tests indicate that modern turbogenerators manufactured by the company with which the writers are associated operate successfully in the suggested area for voltage regulator lower limit setting without excessive temperatures and retain adequate stability margins. It should be recognized that for hydrogen-cooled machines, operation with increased hydrogen pressure permits a higher kilovolt-ampere output under comparable operating conditions.

Manual Control Operation

Figures 4 through 9 illustrate the factors influencing pullout power when the generator excitation is manually controlled. Referring to Figure 2, the generator excitation voltage E_d and the system voltage E_s are determined so that with the initial loading there is rated voltage at the generator terminals, except for Figure 6 for which the initial generator voltage is varied. Holding E_d and E_s at their initial values, the maximum pullout power is assumed to occur when these voltages are at 90 degrees. Figures 4 through 9 show the pullout power that can be developed with the excitation and system voltages unchanged from their initial values for different initial loads, initial power factors, system equivalent reactances, generator short-circuit ratios, and initial generator terminal voltages.

Figure 4 shows the pullout power versus power factor for several constant kilowatt loads. The power increment between the curves and the dashed line at the kilowatt loading for which the curve is drawn is the power that may be picked up under the condition of no change in excitation or system voltage from their initial values. These power increments have been plotted against power factor in Figure 5. The effect of changing generator terminal voltage on allowable power increment is shown in Figure 6. Pullout power as function of initial kilowatt load for different initial power factors is shown in Figure 7. The curves of Figure 8 illustrate the effect of system reactance on pullout power. The effect of generator short-circuit ratio on pullout power is shown in Figure 9. Figure 10 shows the variation in pullout power produced by a change in system equivalent reactance for a low short-circuit ratio generator.

The question of primary importance in the system operation is the ability of its component parts to endure the normally encountered emergencies. Generators will be required to quickly pick up load in case a generator is tripped off; in case a major tie-line opens; or if a high rate of system load increase is encountered. For most system conditions, it can readily be determined what load increases may be expected on the several generators as a result of any emergency. This determination should take into account any

Figure 3. Rated voltage generator circle diagram
0.8 power factor cylindrical rotor generator
Short circuit ratio of generator = 0.9
Equivalent X_d = 1/TCR = 1.111
Maximum turbine output = generator kilovolt-amphere

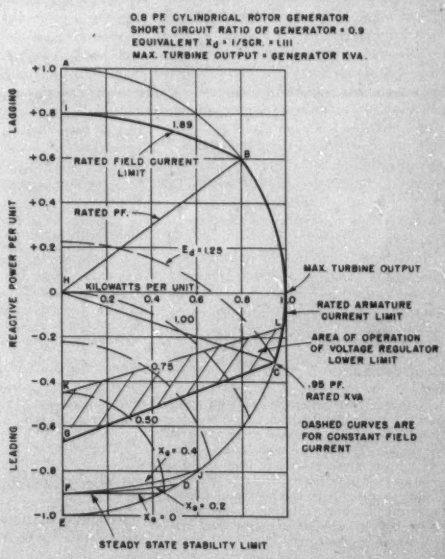
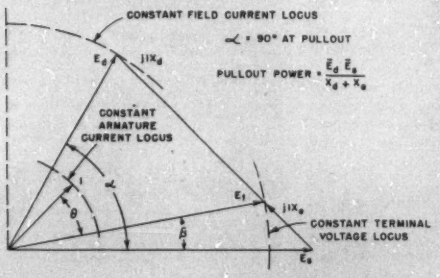
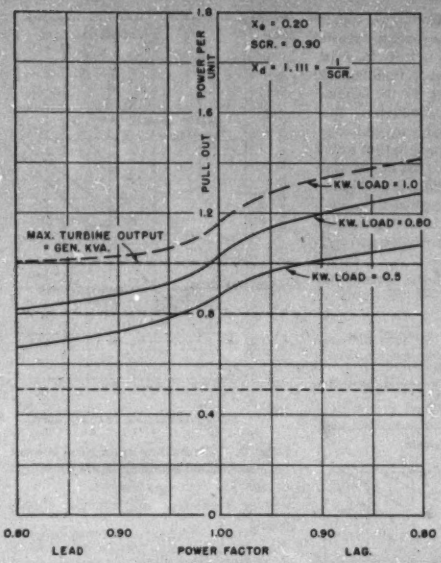


Figure 2. System vector diagram





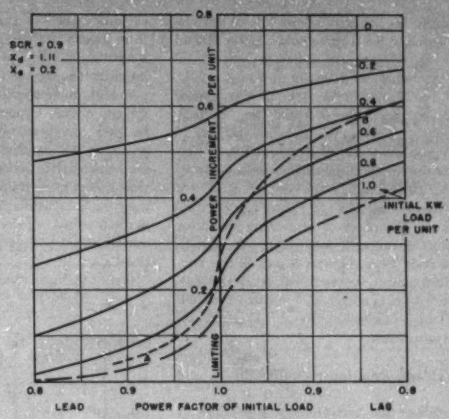


Figure 4 (left). Pullout power versus power factor at constant kilowatt loads

Figure 5 (above). Limiting value of power increment for stability versus initial load power factor for different initial loads

limitations in boiler capacity; speed governor characteristics; and the action of any load-limiting devices which may be employed in the control of the prime-mover input. In this way an estimate can be made of excitations which should be maintained for machines on manual control in order that they may pick up their expected share of load without loss of synchronism.

For a typical machine, Figure 4 shows that a machine initially loaded to 80 per cent kilowatts at 0.9 power factor lead can pick up about 7 per cent in power before going out of step. It also shows that with 80 per cent initial kilowatt load at 1.0 power factor, the pullout power exceeds the turbine capability. Margins in permissible power increment are small in the higher load regions at 0.8 power factor leading.

If the generator load increase for the most severe system disturbance is known, the required power factor for the initial kilowatt load may be obtained from Figure 5. The dashed curve AB on Figure 5 is drawn through the points where the initial kilowatt load plus the permissible power increase is equal to the maximum turbine capability. If operation is maintained in the region to the right of curve AB, the generator will not pull out within the turbine rating. This type of operation would be most conservative. It is also apparent from Figure 5 that with an initial 20 per cent kilowatt load at 0.9 leading power factor, a 50 per cent load increase can be accepted by the generator before instability is encountered.

In practical operation, the generator voltage will probably be less than rated when the machine is required to absorb large amounts of leading reactive kilovolt-amperes. Figure 6 shows the effect generator terminal voltage has on permissible power increments for power factors between 0.95 lag and 0.95 lead. The desirability of voltage regulator control when operating in the leading region and at low generator terminal voltage is emphasized by the reduction in allowable power increment as the generator voltage decreases. At 0.95 leading power factor and 0.8-kw load, the power increment at 90 per cent generator voltage is one-half its value at rated generator voltage.

In Figure 7 the difference between the straight line at 45degrees with the horizontal axis and the curves may be interpreted at any kilowatt loading as the permissible power increment at that initial loading. The increase in the power increment as initial power factor goes from the leading to the lagging region is evident. To accept the maximum turbine output, an

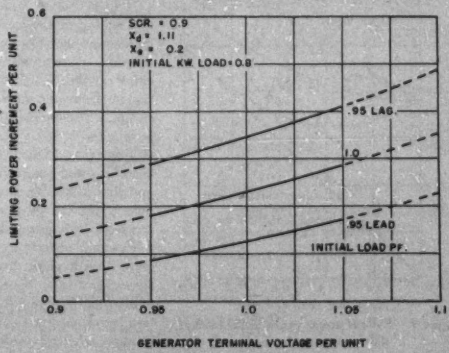


Figure 6. Limiting value of power increment for stability versus generator terminal voltage

initial 40 per cent kilowatt load may be carried at 0.8 lagging power factor and an initial 75 per cent kilowatt load at 1.0 power factor.

In Figures 8 and 9 the permissible power increment is again the difference between the curves and the straight 45 degree line. The change in pullout power for an initial 1.0 power factor load resulting from doubling the equivalent system reactance is shown in Figure 8. Figure 9 shows the effect of decreasing generator short-circuit ratio from 0.9 to 0.6. Qualitatively from Figures 8 and 9, the conclusion may be drawn that generator pullout power is not greatly affected by small changes in its short circuit ratio or in system stiffness.

Voltage Regulator Operation

When the excitation of a machine is under the control of a voltage regulator, adjusted to maintain rated voltage at the machine terminals, the generator will stably accept the maximum turbine output in any practical case. The curves in Figure 10 assume that the excitation system has a ceiling voltage 50 per cent in excess of the required slip ring voltage at the rating of the main generator. This is equivalent to having an exciter of 300-volt ceiling and rated generator slip ring volts of 200 which is representative of present day practice. Terminal voltage of the generator is assumed to be 1.0; the exciter is at ceiling; 90 degrees exists between machine excitation voltage and equivalent system voltage. For ceiling excitation on the generator, it appears from Figure 10 that the available generator pullout power is about twice the maximum turbine output for X_s from 20 to 40 per cent corresponding to the average system under normal conditions. It is realized that these torques are seldom required or seldom exist and to that extent are somewhat fictitious, but at the same time it does give an indication of the margins which are available through the automatic control of excitation, which are available to meet emergency system conditions.

Some voltage regulators permit the

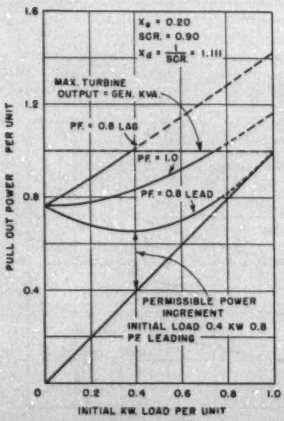


Figure 7. Pullout power versus initial load at different power factors

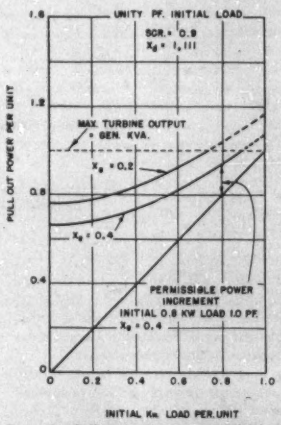


Figure 8. Pullout power versus initial kilowatt load for different external reactance

adjustment of the lower limit of excitation as a function of turbine kilowatt load or of the generator kilovar output. Area *CGKL* on Figure 3 suggests a range for the setting of these limits which will in most cases leave a considerable margin between system requirements and machine capability. Manual control operation in this region allows the generators to accept a reasonable load increase before pulling out of step; however, it should be realized that Figure 3 represents conditions for rated generator terminal voltage, and that allowable power increments are reduced as the generator voltage is decreased. Voltage regulator control rationally permits operation closer to the steady-state stability limit and possibly beyond those shown by operation in the "dynamic" stability region. However, operation below the line *GC* on Figure 3 should be approached advisedly from the system,

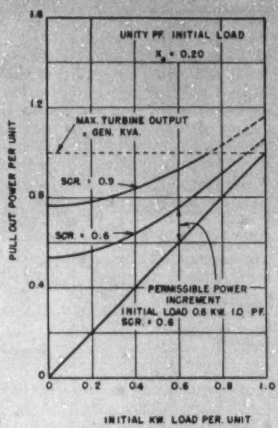


Figure 9. Pullout power versus initial kilowatt load for different short-circuit ratios

voltage regulator, and machine standpoints

Transient Stability

Since the voltage behind transient reactance is not affected greatly by power factor at rated terminal voltage, transient stability limits are almost unaffected by initial loading conditions. Voltage regulators with usual rates of response and ceilings affect these limits by a rather minor amount. Of course, transient synchronizing torques are high as compared to the steady-state stability limits and are of importance in determining whether synchronism will be lost after a system short circuit has been cleared.

For a turbogenerator on an average system operation at unity or in the leading power factor region will have a small effect upon transient limits. With manual control of excitation, generators should be operated with sufficient excitation so that they can stably accept their load increment after the transient phase is over. Voltage regulators can adjust excitation rapidly enough during the transient period, so that any load may be assumed up to the generator rating. Voltage regulator control permits operation closer to the steady-state stability limit.

Nomenclature

Unit Kva = unit kilovolt-amperes is the generator rated kilovolt-amperes which is equal to the maximum rated turbine output

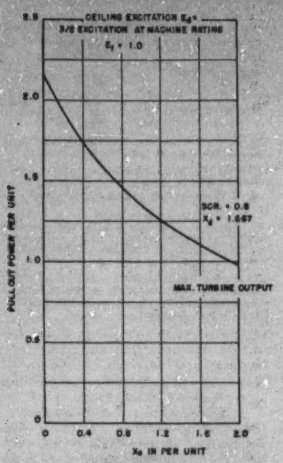


Figure 10. Pullout power versus external reactance

P = real power in per unit
Q = reactive kilovolt-amperes in per unit
E_d = generator excitation voltage which is proportional to generator field current. Unit excitation voltage produces the field current required at rated no load voltage on the generator saturation curve
E_t = generator terminal voltage in per unit of rated generator voltage
E_s = equivalent system voltage in per unit of rated generator voltage
SCR = generator short circuit ratio is the ratio of field current at no load rated voltage on generator saturation curve to field current that produces rated generator armature current with armature short circuited
X_d = equivalent generator synchronous reactance in per unit which is assumed equal to reciprocal of generator short circuit ratio
X_e = equivalent system reactance in per unit of generator base ohms. Base ohms of generator equals rated generator line to neutral voltage divided by rated generator armature current
I = generator armature current in per unit of rated armature current
θ, β, α = angles defined by Figure 2

Appendix I

Figure 3 shows approximate reactive and real power limits on the basis of rated voltage at the machine terminals. These limits may be readily estimated in the following manner:

Rated Armature Current Limit

The vector power is given in equation 1 for the convention that an overexcited generator supplies positive lagging reactive power to its connected system†

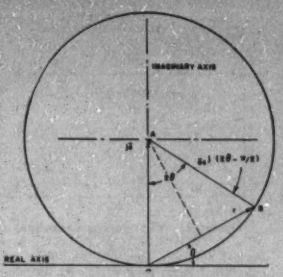


Figure 11. Circle in vector notation

$$\mathbf{P}+j\mathbf{Q}=\mathbf{E}_t\mathbf{I}^* \tag{1}$$

Referring to Figure 2, the generator terminal voltage in vector notation is

$$\mathbf{E}_t=E_te^{j\beta} \tag{2}$$

and the generator armature current is similarly

$$\mathbf{I}=Ie^{j(\beta+\theta)} \tag{3}$$

The conjugate of the current is

$$\mathbf{I}^*=Ie^{-j(\beta+\theta)} \tag{4}$$

The vector power is found by substituting equations 4 and 2 in equation (1).

$$\mathbf{P}+j\mathbf{Q}=E_te^{j\beta}Ie^{-j(\beta+\theta)} \tag{5}$$

or

$$\mathbf{P}+j\mathbf{Q}=E_tIe^{-j\theta} \tag{6}$$

Equation 6 for the condition that $E=1.0$ and $I=1.0$ (rated voltage at the generator terminals and rated current in armature), represents a circle with its center at the origin with a radius of $E_tI=1.0$. This is circle $ABCDE$ on Figure 3. When the current lags the terminal voltage, the imaginary part of equation 6 is positive since the angle θ is negative. This corresponds to overexcited operation.

Rated Field Current Limit

The maximum field current is determined by the field current required when the machine is carrying rated kilovolt-ampere at rated power factor. Referring to Figure 2 and determining the vector power in the same manner:

$$\mathbf{P}+j\mathbf{Q}+\mathbf{E}_t\mathbf{I}^* \tag{7}$$

$$\mathbf{I}=\frac{\mathbf{E}_d-\mathbf{E}_t}{jX_d}=\frac{E_de^{j\alpha}-E_te^{j\beta}}{jX_d} \tag{8}$$

$$\mathbf{I}^*=\frac{E_de^{-j\alpha}-E_te^{-j\beta}}{-jX_d} \tag{9}$$

$$\mathbf{P}+j\mathbf{Q}=E_te^{j\beta}\left[\frac{E_de^{-j\alpha}-E_te^{-j\beta}}{-jX_d}\right] \tag{10}$$

Multiplying out equation 10

$$\mathbf{P}+j\mathbf{Q}=\frac{E_tE_d}{X_d}e^{j\left(\beta-\alpha+\frac{\pi}{2}\right)}-j\frac{E_t^2}{X_d} \tag{11}$$

For constant values of E_d and E_t, equation 11 is a circle with its center on the negative reactive axis at E_t^2/X_d and with a radius of E_tE_d/X_d. Applying to Figure 3, the center of circle IB is on the negative reactive axis at $E_t^2/X_d=1/1.111=0.9$, and its radius is $E_tE_d/X_d=(1)(1.89)/1.111=1.70$. Circles for other constant field currents than rated may be easily drawn by keeping the same center and varying the length of the radius in proportion to the field current.

Steady-State Stability Limit

The limiting pullout power is taken to be that power flow existing when E_d and E_s are at 90 degrees and $E_t=1.0$. For this case taking E_t along the real axis,

$$\mathbf{E}_d=E_de^{j\alpha}=E_de^{j\pi/2}=jE_d \tag{12}$$

$$\mathbf{E}_s=E_se^{j\beta} \tag{13}$$

$$\mathbf{I}=\frac{\mathbf{E}_d-\mathbf{E}_t}{jX_d}=\frac{jE_d-E_te^{j\beta}}{jX_d} \tag{14}$$

$$\mathbf{I}^*=\frac{-jE_d-E_te^{-j\beta}}{-jX_d}=\frac{E_d-jE_te^{-j\beta}}{X_d} \tag{15}$$

$$\mathbf{P}+j\mathbf{Q}=\mathbf{E}_t\mathbf{I}^*=E_te^{j\beta}\left[\frac{E_d-jE_te^{-j\beta}}{X_d}\right] \tag{16}$$

$$\mathbf{P}+j\mathbf{Q}=\frac{E_tE_de^{j\beta}}{X_d}-j\frac{E_t^2}{X_d} \tag{17}$$

From the geometry of Figure 12

$$\sin\beta=\left[\frac{X_e}{X_d+X_e}\right]\frac{E_d}{E_t} \tag{18}$$

or

$$E_d=\left[\frac{X_d+X_e}{X_e}\right]\sin\beta E_t \tag{19}$$

Eliminating E_d from equation 17 by using equation 19

$$\mathbf{P}+j\mathbf{Q}=E_t\left[\frac{X_d+X_e}{X_eX_d}\right]\sin\beta e^{j\beta}-j\frac{E_t^2}{X_d} \tag{20}$$

By using the relation developed in Appendix II, equation 20 is:

$$\mathbf{P}+j\mathbf{Q}=E_t\left[\frac{X_d+X_e}{2X_eX_d}\right]e^{j(2\beta-\pi/2)}+jE_t^2\left[\frac{X_d+X_e}{2X_eX_d}-\frac{1}{X_d}\right] \tag{21}$$

Equation 21 is the equation of a circle with its center on the reactive axis at

$$E_t^2\left[\frac{X_d+X_e}{2X_eX_d}-\frac{1}{X_d}\right]$$

and a radius of

$$E_t^2\left[\frac{X_d+X_e}{2X_eX_d}\right]$$

In Figure 3, the center of circle DF is at

$$(1)^2\left[\frac{(1.111+0.2)}{2(0.2)(1.111)}-\frac{1}{1.111}\right]=2.045$$

on the positive reactive kilovolt-ampere axis and has a radius of

$$\frac{(1)^2(1.111+0.2)}{2(0.2)(1.1111)}=2.945$$

† See Nomenclature for definitions of symbols.

* Asterisk adjacent to bold face type indicates a conjugate vector.

Appendix II

Referring to Figure 11, a circle is described by a vector when its scalar magnitude remains constant and its angle completes 360 degrees. A circle passing through the origin with radius a and center on the imaginary axis is represented by equation 22.

$$r = ae^{j(2\theta - \pi/2)} + ja \qquad (22)$$

From the geometry of Figure 12, the scalar magnitude of r is that shown in equation 23.

$$r = 2a \sin \theta \qquad (23)$$

The angle subtended at A by the arc OB is 2θ, where θ is the angle that the radius vector makes with the real axis. Therefore in vector notation equation 24 shows two equivalent forms for a circle with its center at a on the

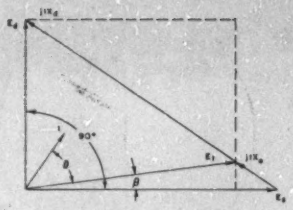


Figure 12. Vector diagram at pullout

$$r = 2a \sin \theta e^{j\theta} = ae^{j(2\theta - \pi/2)} + ja \qquad (24)$$

imaginary axis and radius of a. The center of the circle may be shifted up and down the imaginary axis by the simple addition of another vector.

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Discussion

M. D. Ross (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The authors are to be congratulated on giving a very interesting treatment of stability limits in the underexcited region of operation. The capability curves included in the AIEE standards for the larger machines were proposed to the committee by McClure and have been a valuable addition to the standards in explaining the load limits of the turbine generator. Figure 3 of the present paper expands that diagram to the underexcited region.

In discussing the limitations of the generator so far as heating of the end zones is concerned the authors, in the second paragraph under the heading "Generator Operating Limits" would infer that improvements in the design included the use of non-magnetic materials for the stator end plates or clamping flanges and perhaps for the retaining rings of the rotor. It might be of interest to state the experience of the company with which I am associated in which we have always used magnetic materials for stator end plates and rotor retaining rings and continue to do so today.

For a given load at leading power factor the flux in the end zones is largely governed by the long air gap between the rotor and the stator end plates in the end of the stator core. This flux, which rotates with its rotor, passes from one pole of the rotor to the end of the stator, passes circumferentially around the stator, out the opposite side of the stator to the opposite pole and through the rotor to its starting point. If a nonmagnetic end plate is used a considerable portion of this flux passes through the end plate into the end laminations of the core, producing losses in these parts. Where a magnetic end plate is provided, this flux passes around the stator in the end plate in a thin skin on the surface of the solid iron. It is our practice to use alloy steel which will reduce the losses in the magnetic end plate to a reasonable value. In this case practically no flux enters the laminations back of the end plate.

We tested a 25,000 kva hydrogen cooled, 3,600 rpm generator about a year ago at a number of armature current values at zero power factor underexcited, and found the temperature rise of the stator end plates with zero field and 90 per cent armature current was below 60 degrees centigrade as compared with an American Standards Association temperature limit for such parts of 70 degree centigrade rise. While this temperature rise will not necessarily be duplicated in all sizes of machines, we have to date found no difficulty in building the largest machines with end plates and retaining rings of magnetic materials.

Table I. Data on Tests to Determine Extent of Armature Lamination Finger Heating

Megawatts	Field Amp	Power Factor	Temperature Rise, Degree Centigrade: Armature	Field	Hottest Finger
42.3	470	0.97 Lag	33.7	50.5	59.5
42.8	350	0.97 Lead	33.0	36.5	84.7

Note: Armature temperature by embedded detector. Field temperature by change in resistance.

Machine rated 44,705-kva 0.85-power factor, 13,200 volts

Matthew S. Schneider (The Cincinnati Gas and Electric Company, Cincinnati, Ohio): The authors offer a word of caution in regard to the operation of machines made prior to 1925 at low excitation. An investigation of the insulation failure in one of these older machines led to the conclusion that extensive finger heating was the cause.

The magnetic structure mentioned consists of armature lamination clamping fingers of magnetic material held in place by a ring of the same material. The fingers are so situated in the machine that they are in the flux area of the main field retaining ring. The amount of flux through the fingers is a function of the saturation of the ring due to d-c field current. The greater the field current the lower will be the flux in the fingers and the less the heating effect produced by iron losses. The maximum heating occurs where the a-c flux density is greatest, which in the machine that failed is at the center of the phase belts.

This local heating effect will not be observed in readings of the usual embedded temperature detectors and will be apparent only by flow of compound out of insulation adjacent to the hot fingers.

To determine the extent of this heating, tests were conducted on a machine which had identical characteristics as the one which failed. Thermocouples were inserted in the fingers to obtain actual temperatures. The data in Table I, giving the test results, show the extent of this heating.

Not only was the temperature in the clamping fingers higher than that of the other parts of the machine, but the rate of rise was also greater. The finger temperature change followed the field current change by a matter of seconds.

J. W. Batchelor (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The generator capability curve illustrated in Figure 3 of the paper for which the authors have presented an admirable derivation is quite useful to the designer and the application engineer. It is suggested that a derivation with a different approach might bring out some features which would make the information as useful to others as it has proved to us.

This derivation is based upon the concept of an unsaturated machine, as suggested by the authors, in which the no-load field current is equal to that of a machine with saturation. Thus, the reciprocal of the short circuit ratio equals the equivalent synchronous reactance. If the excitation for the synchronous reactance is designated as 1.0, the no-load excitation equals the short circuit ratio. These two values of excitation may then be added at the rated power factor angle to determine rated full load excitation.

Therefore, it is necessary to know power factor, short-circuit ratio and system reactance at rated kilovolt-ampere, to determine the per unit generator capability curve as shown in Figure 1 of the discussion. The curve ACF with center at H and radius equal to 1.0 represents the full load temperature limit of the armature. Full load field current may be determined by adding the field currents for no-load and synchronous reactance at the rated power factor angle. This is the triangle GHB and since GB is the full load field current, the curve BJ with center at G represents the maximum loading in the region where field temperature is the limit.

The steady-state stability limit is an arc

starting at G with center along the vertical axis, and with radius equal to $SCR/2+1/2X_s$, where X_s is the system reactance on the machine kilovolt-ampere base. The expression for the radius may be obtained by considering the condition for pullout at machine voltage and power factor equal to 1.0, and by solving similar right triangles. The expression derived by the authors may be reduced to this form. In the case of an infinite bus, the circle has an infinite radius, and the limit reduces to the line GE.

Once the capability curve has been established for full load conditions, it may be used for other operating conditions, by keeping the center for armature conditions at H and for field conditions at G. For example, with a hydrogen cooled turbine generator nominally rated at 0.5 pound per square inch gauge gas pressure, but operating at 15 pounds per square inch gauge the stator current may be increased 15 per cent and the field current increased 8 per cent. Thus, the radius HC is increased 15 per cent, and radius GB is increased 8 per cent. The intersection of these two circles is on an extension of the line HB. It should be noted that the per unit scale for field current is not necessarily the same scale as is used for kilovolt-amperes since one per unit field current is determined by the short circuit ratio.

J. B. Ward (Purdue University, Lafayette, Ind.): This timely paper will be very useful to engineers concerned with system operating and planning problems. The curves in the paper clearly show the influence of various factors upon operating limits at low excitation. It is the purpose of this discussion to examine more critically the simplified method of analysis used to derive these curves. The authors list the assumptions on which the method is based, but the conclusion that these do not seriously affect accuracy over a wide range of system conditions is not immediately obvious. At least the range of applicability is not clearly evident in the assumptions themselves, and a planning engineer might wonder, first of all, if his particular system and operating conditions would fall within the range referred to by the authors. In addition, rational decisions on safe operating margins which are based on stability calculations require some knowledge of limitations and potential errors inherent in the method used.

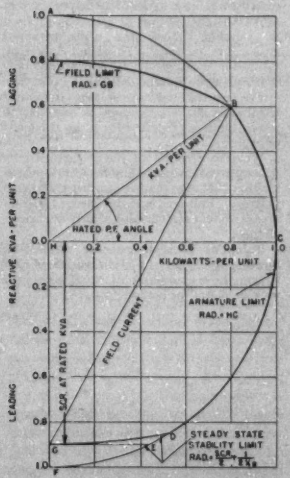

Figure 1. Generator rated voltage capability curve

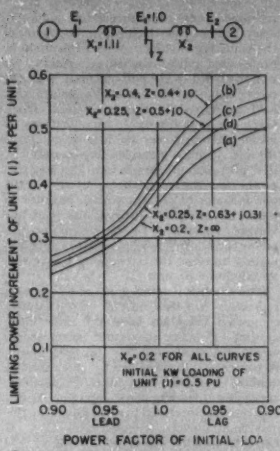

Figure 2. Limiting power increment for generator (1) as a function of initial power factor at initial kilowatt loading of 0.5 per unit. All impedance and power quantities in per unit on machine (1) kilovolt-ampere rating

In connection with the third assumption of the list, the method of representing the system by an equivalent reactance for stability calculations is fairly well known, but perhaps is not as well understood. Basically it is an application of Thevenin's theorem, but unfortunately with the presence of speed governors, magnetic saturation, and other influences, power systems do not submit to rigorous application of linear network theory. With most of the reactance residing in the machine to be tested for pull-out limits and a relatively small amount in the equivalent system it seems logical to assume that system conditions would not seriously affect results. Nevertheless, it is desirable to know how much and in what direction the pull-out limits might vary with system conditions. This variation can be demonstrated to a certain extent by numerical examples based on equivalent circuits which represent a slightly less complete reduction of the system than the single equivalent reactance. As an illustration, the following examples are presented, giving some results for the relatively stiff equivalent reactance of 0.2 per unit which is discussed in detail in the paper as an average condition.

Consider first a metropolitan-type system in which the generating capacity is close to a concentrated load area. The other extreme of a remote station supplying power over a high-voltage transmission link is discussed later. Referring to the equivalent circuit in Figure 2 of the discussion, all generation except unit (1), the machine being studied, is combined into an equivalent generator as shown at (2). This assumption is valid if the load is almost evenly divided among machines of similar characteristics. The system load is combined also into a single equivalent load represented by a constant impedance Z. An additional step of paralleling Z and X_2 gives the equivalent reactance X_s of the system. In this circuit the magnitude of the system load and the relative loading of connected generating capacity are not entirely concealed. Upon specifying the terminal voltage and the initial loading and power factor of unit (1), the loading of the equivalent generator and the excitation voltages E_1 and E_2 can be determined. The pull-out power at constant excitation is obtained from the equation:

$$\text{Max. } P_1=\frac{E_1^2}{Z_{11}}\sin\alpha_{11}+\frac{E_1E_2}{Z_{12}}$$

where: Z_{11} = magnitude of the driving point impedance viewed from E_1

Z_{11} = magnitude of the driving point impedance viewed from E_1

$=X_1+X_2Z/(X_2+Z)$

Z_{12} = magnitude of the transfer impedance from E_1 to E_2

$=X_1+X_2+X_1X_2/Z$

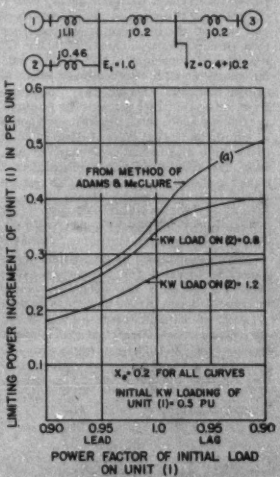

Figure 3. Limiting power increment for generator (1) as a function of initial power factor at initial kilowatt loading of 0.5 per unit on (1) and with constant kilowatt loading on (2). All impedance and power quantities in per unit on machine (1) kilovolt ampere rating

$\alpha_{11} = 90° - \theta_{11}$

θ_{11} = impedance angle of Z_{11}

The curves of Figure 2 of the discussion show some results for an initial kilowatt loading on machine (1) of 0.5 per unit. The values assumed for this machine are: Rated power factor 0.8, *SCR* 0.9, $X_1 = 1/SCR$, initial terminal voltage 1.0 per unit. The various combinations of X_2 and Z shown with the four curves are chosen to give a value of 0.2 per unit for X_e in each case. All quantities are based on the kilovolt-ampere rating of machine (1).

Curve (a) is obtained by the simplified method presented in the paper. For curve (b) the load is 2.5 per unit, unity power factor, resulting in an initial kilowatt loading of 2.0 per unit on the equivalent machine. If X_2 represents generators with the same *SCR* as unit (1), this corresponds to nearly full load on the connected capacity. Curve (c) represents a different system in which the $0.5+j0$ load impedance corresponds to 2.0 per unit system load and results in a 1.5 per unit loading on the equivalent machine. This is a loading of approximately one third the connected capacity on the same basis. Curve (d) is for the same value of X_2 as curve (c) but with a system load at 0.9 power factor lagging. Note that all three curves lie above (a), indicating approximately the extent by which the simplified method described by Adams and McClure gives conservative results for these system conditions.

Next consider a system of the type illustrated in Figure 3 of this discussion. The machines represented by (1) and (2) supply power to a remote load center (Z) over a transmission system, and the generators at and beyond the load center are combined into an equivalent machine (3). Unit (1) represents the machine to be tested for pull-out limits and (2) the combined effect of nearby machines. If unit (1) is a 50-megawatt, 0.8 power factor machine, for example, the line reactance of 0.2 per unit corresponds to approximately 80 miles of double-circuit 132-kv line with transformer banks at each end. For simplicity resistance is neglected except in the load impedance. This system, viewed from the terminals of unit (1), again reduces to an equivalent reactance X_e of 0.2 per unit. Ratings of unit (1) are the same as in Figure 2.

Curve (a), obtained by the method of the paper, is included for comparison. The other curves show the maximum power increments which can be picked up by unit (1) from an initial kilowatt loading of 0.5 per unit with two different constant kilowatt loadings on (2). These results were obtained by means of an a-c network calculator. In the calculator set-up capacitive reactance was substituted for inductive reactance to avoid the inherent resistance of the inductive circuits. After adjustment to the desired initial conditions, machine (1) was advanced to the pull-out limit, with the power output of (2) and excitation voltages held constant. In this equivalent circuit there are many possible combinations of initial reactive power loadings on machines (2) and (3) for a given loading on unit (1). For purposes of comparison the reactive power was shifted from (1) to (2) as the power factor of (1) was varied, in order to keep the initial line loading and load bus voltage constant. In both curves the power factor of (2) was adjusted to approximately unity at the 0.9 lagging power factor condition on unit (1).

The kilowatt loading on (2) of 1.2 per unit represents half-load on the basis that the connected machines have the same *SCR* as unit (1). The maximum power increments are considerably less at lagging power factors than those obtained by the simplified method, but with a smaller discrepancy appearing at leading power factors. As the load on (2) is increased beyond this value the maximum power increments for (1) fall decrease rapidly since the system is approaching the steady-state stability limit. For 0.8 per unit kilowatt load on (2), a less severe condition, the curve conforms more closely with that of the simplified method, particularly in the leading power factor region.

For the conditions considered before, the single equivalent reactance representation yields sufficiently accurate results for most practical purposes at unity and leading power factors, which is the area of primary interest in the present paper. The simplified method probably should be applied with caution to initial loadings in the lagging region when long, heavily loaded transmission systems are involved. The applicability to other operating conditions and types of systems can be checked in a similar manner.

W. H. Osterle (West Penn Power Company, Pittsburgh, Pa.): As I understand this paper, the authors have taken the mathematical equation $E_d E_t / X_d + X_e$ and for different loads and various power factors with different values of external impedance (X_e) calculated the pull-out power, using X_d, the reciprocal of the short-circuit ratio, and E_t, terminal voltage of the machine, as unity. Have the authors any experimental data or results of any actual cases of generator static instability that can be plotted on any of these curves to see how near actual cases compare?

Will the authors elaborate a little on the subject of "end structure heating with low field current?" How serious is this condition and what information is needed to detect its presence?

Under the section Voltage Regulator Operation the following sentence appears:

> "When the excitation of a machine is under the control of a voltage regulator, adjusted to maintain rated voltage at the machine terminals, the generator will stably accept the maximum turbine output in any practical case."

I understand this to mean that a generator under the control of a voltage regulator cannot become unstable when operating at rated voltage. (1 per unit.) Suppose the generator were operating at a voltage of say 0.95 or 1.05 per unit, would this have any effect on its stability characteristics?

C. G. Adams and J. B. McClure: The authors welcome the supporting data and calculations submitted in several of the discussions. In general, they serve to justify some of the assumptions and emphasize limitations on older turbogenerators in the underexcited region.

Ross has presented valuable data regarding zero power factor operation of modern generators which his company manufactures. With regard to detailed generator design to avoid serious stator end heating, there are several design approaches. As well as the one outlined by Ross, the use of nonmagnetic end materials and step backs in the stator core is successful.

Schneider's data clearly show that serious heating can develop at leading power factors in older machines. Our tests also show that the finger temperatures rapidly follow changes in loading. When underexcited operation is contemplated on older machines in service, field tests provide the best data on which to base future operation.

Ward's discussion supports the assumption of a generator equivalent reactance equal to the reciprocal of its short circuit ratio, especially at leading power factors. Obviously the purpose of this assumption is to simplify calculations while retaining reasonable accuracy. In actual operation, borderline cases should be approached only when using automatic control of excitation.

The use of modern voltage regulators eliminates the significance of incremental power limits established for manual control of excitation.

We refer Osterle to data presented by other discussers and to Figure 6 in the paper for the effect of operating at other than rated voltage. Voltage regulator control will increase system stability for steady-state load changes.

Batchelor presents another method of arriving at Figure 3. In the use of this figure, the assumption of rated generator voltage should be borne in mind.

Rototrol Excitation Systems

J. E. BARKLE
ASSOCIATE AIEE

C. E. VALENTINE
MEMBER AIEE

FOR purposes of discussion, an excitation system for a synchronous machine may be considered as including all equipment necessary to regulate the a-c voltage and supply excitation to the field of the a-c machine. This will include the voltage regulator and its auxiliary devices, the d-c machines for supplying excitation to the a-c machine and provision for manual control of the a-c machine output. The development of the Rototrol amplifier as a portion of the a-c machine excitation system has brought about radical changes in all three parts of the excitation system.

Rototrols for excitation systems are available having single-stage or two-stage amplification. The principle of operation of both types has been described previously along with analyses of their use in several industrial applications.[1,2] The choice between one-stage or two-stage amplification depends on the energy available in the control circuit and the total power output required. The two-stage Rototrol is capable of producing sufficient power at its terminals to excite the field of the largest steam-driven generator with approximately the same controlling energy used with the single-stage machine. Therefore, the two-stage Rototrol can be used as either a pilot exciter or a main exciter.

The Rototrol is similar in design and appearance to a conventional d-c machine. Control and amplification are obtained through the use of several stator fields.[2] Mechanical construction is essentially the same as any conventional d-c machine of similar rating and speed. The physical size of a Rototrol pilot or main exciter is substantially the same, or slightly larger than conventional machines due to more field windings in the stator structure and due to operation with an unsaturated magnetic circuit. The air gaps of both types of machine are of normal size. Either one or two stages of amplification are supplied in a four-pole machine; therefore, the Rototrol can be direct-connected to the shaft of any generator operating at 1,200, 1,800, or 3,600 rpm. In providing excitation for slower speed machines, the Rototrol must be separately driven at higher speed to secure a smaller physical size unit, which will not require excessive control energy.

The following discussion explains how the Rototrol pilot exciter is used in the excitation systems of various types of a-c machines. Since the pilot exciter and main exciter are only part of the excitation system, the various types of voltage regulators and limiting devices are included to make the discussion complete. The Rototrol direct-connected main exciter is a radical departure from previous practices in providing excitation for large turbine generators. A complete description and analysis of performance is being presented in a companion paper[3] and only brief mention made in the following discussion.

Turbine Generators

Rototrol Pilot Exciter with Single-Field Main Exciter

The simplest form of Rototrol pilot exciter as it may be used with a turbine generator is shown schematically in Figure 1. Since the Rototrol is a high-speed machine and of normal d-c machine construction, the a-c generator, main exciter and pilot exciter are all driven by the same shaft at 1,200, 1,800, or 3,600 rpm (for a 60-cycle generator).

In the conventional excitation system, the pilot exciter is a constant-voltage generator. The Rototrol pilot exciter is a variable-voltage pilot exciter and the method of operating the excitation system is essentially no different than the operation of conventional exciter-rheostatic systems, except in the new arrangement. no regulator-controlled, motor-operated exciter-field rheostat is used. Variable voltage is supplied to the main-exciter field by the Rototrol pilot exciter which is connected directly to the field and is under the control of the voltage regulator automatic control unit or the manual control unit.

The voltage regulator potential unit measures the positive-sequence voltage at the terminals of the generator enabling the regulator to regulate the polyphase voltages, rather than choosing the voltage of one particular phase. The voltage adjusting unit is a small hand-operated rheostat in the low-voltage circuit and is used to set the regulated voltage at the desired level at any time.

Particular attention is directed to the voltage regulator automatic control unit, which is very different from previous control units used with conventional pilot exciters. The exciter-rheostatic excitation system uses a contact-making voltage measuring unit which controls the motor-operated, main-exciter field rheostat through normal and quick raise and lower contactors. Several moving parts and contactors are used. The automatic control unit used with the Rototrol pilot exciter is a static device and completely eliminates all moving parts and all contactors. An impedance type of static network voltage regulator generally is used with generator excitation systems.

The manual control unit consists of a bridge circuit excited by the voltage drop across the main-exciter field. Again, the Rototrol control field is differentially connected in the bridge circuit. Deviation of the main-exciter shunt field voltage from its hand-set value causes an unbalance in the bridge circuit and current flows in the Rototrol control field to cor-

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J. E. Barkle is central station engineer and C. E. Valentine is with the switchgear and control engineering division, both with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

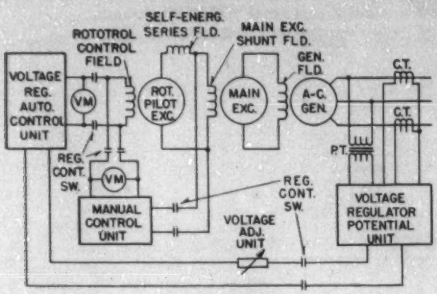


Figure 1. Excitation system with Rototrol pilot exciter and single-field main exciter

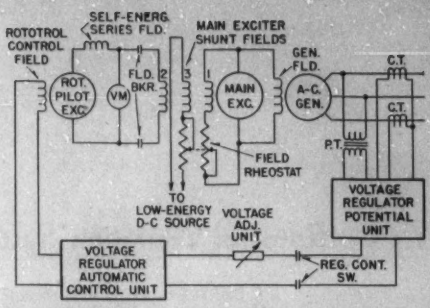


Figure 2. Excitation system with Rototrol buck-boost pilot exciter and three-field main exciter

rect the voltage. The manual control unit, therefore, regulates the main-exciter shunt-field voltage to maintain it constant at any value set by the operator without further attention on his part.

The Rototrol pilot exciter used in this excitation system may provide either one or two stages of amplification, depending on the energy requirements of the main-exciter shunt field. The self-energized series field of the Rototrol provides all the excitation requirements of the pilot exciter when the a-c generator is operating with regulated voltage output. However, since the Rototrol operates on the linear portion of its saturation curve, the control field provides the necessary stabilizing effect and holds the Rototrol voltage at a constant value.

The Rototrol can easily be constructed to provide rates of response and ceiling voltages equal to or in excess of those obtained with conventional d-c machines. The excitation system shown in Figure 1, therefore, will provide performance characteristics at least equal to those obtained with conventional excitation systems. The series-tuned Rototrol has the advantage of utilizing the induced transient current in the generator field and in the main exciter field due to a-c short circuit or the sudden application or removal of load. The induced current flows through the self-energizing series field of the Rototrol.

The Rototrol pilot exciter, shown in Figure 1, supplies all the excitation requirements of the main exciter. In this respect, this scheme is identical with exciter-rheostatic systems using pilot exciters. The essential advantage is the elimination of the comparatively complicated exciter-rheostatic regulator with its moving parts, and elimination of the motor-operated main-exciter field rheostat. As is the case with the exciter-rheostatic excitation system, loss of the Rototrol pilot exciter through a short circuit or open circuit causes loss of excitation on the a-c generator. Past experience with pilot exciters has shown them to be very reliable. Mechanically and electrically, the Rototrol pilot exciter is as reliable as conventional pilot exciters since standard parts are used throughout in both machines except for the multiple fields in the Rototrol. The main exciter is exactly the same in either type of excitation system.

Rototrol Buck-Boost Pilot Exciter with Multiple-Field Main Exciter

The buck-boost Rototrol excitation scheme using a two- or three-field main exciter as shown in Figure 2, offers a number of advantages over the scheme just described. The voltage regulator potential unit and automatic control unit are the same as before. The Rototrol buck-boost pilot exciter is the same type of machine used in the previous scheme, but operates in a slightly different manner. Operation is best understood by analyzing the performance of the main exciter.

The main exciter is of conventional construction except that it has either two or three shunt field windings instead of the usual single shunt field. Field 1 is a self-excited shunt field providing most of the main exciter field excitation and receiving its energy from the main exciter armature circuit in the usual manner of a self-excited field. Field 3 is a small separately-excited shunt field which obtains its energy from a station battery or any other source of approximately constant d-c voltage. It is capable of supplying 5 to 10 per cent of the normal total excitation requirements of the main exciter, and its purpose is to provide exciter stability at low-voltage output under manual control. It is used only when the exciter speed of response or range of voltage output makes it desirable.

Both field 1 and field 3 have rheostats in their energizing circuits. The rheostat arms are mechanically connected together so that resistance is added in one field circuit while it is removed from the other. The combined rheostat may be either motor-operated, hand-controlled or simply hand-operated. The rheostat for the separately-excited field 3 is open circuited at approximately one-half voltage on the main exciter and remains open-circuited

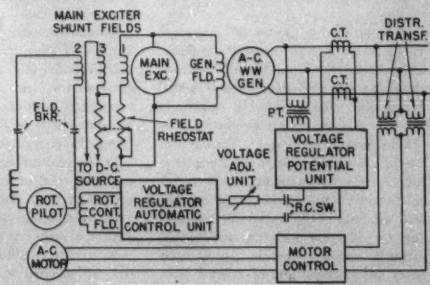


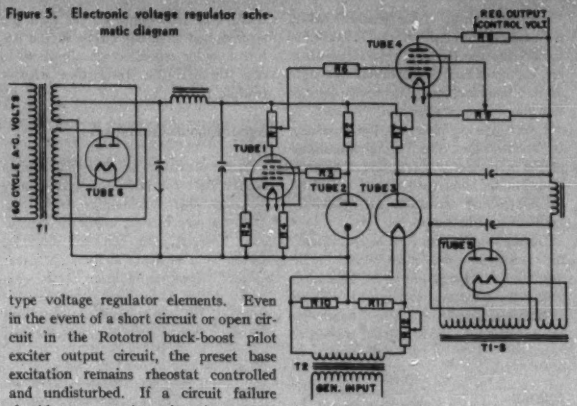
Figure 3. Excitation system for hydroelectric generator with motor-driven Rototrol buck-boost pilot exciter and three-field main exciter

as the exciter voltage is further increased. Field 3 stabilizes the main exciter at low-voltage output under manual control when operation is on the air-gap line, by shifting the total saturation curve so that a positive intersection of the saturation curve with any field resistance line is obtained. Since the stabilizing effect is no longer required at the higher exciter voltage outputs, open circuiting field 3 produces no ill effects and drain on the d-c source for this field is saved.

Field 2 is a shunt field that is excited by the Rototrol buck-boost pilot exciter. In operation with this excitation system, the main-field rheostat in field 1 is set by the operator so that this field alone provides a base value of field current; for example, that sufficient to maintain steady-state stability at full load on the a-c generator. The voltage regulator then controls the Rototrol buck-boost pilot exciter output, both as to polarity and magnitude, so that the excitation provided by field 2 either adds to or subtracts from the base excitation maintaining the predetermined a-c voltage in spite of changing load on the a-c machine. The Rototrol excited field of the main exciter is in itself a stabilizing field under regulator control.

The Rototrol buck-boost pilot exciter is capable of supplying sufficient energy to the Rototrol-excited field on the main exciter so that the net excitation of the main exciter can be controlled by the voltage regulator over the normal load range of the a-c generator. A complete analysis of this excitation system shows that it offers advantages not found in the single-field main exciter scheme previously described or in conventional exciter-rheostatic systems. Since the main exciter base excitation is supplied by the self-excited field, complete excitation is not lost nor is the continuity of the load disturbed upon the occurrence of any trouble in the Rototrol buck-boost pilot exciter circuits or in any part of the static-type voltage regulator elements. Even in the event of a short circuit or open circuit in the Rototrol buck-boost pilot exciter output circuit, the preset base excitation remains rheostat controlled and undisturbed. If a circuit failure should occur at a time when the a-c generator is carrying a load other than that used to determine the rheostat setting, the a-c generator would continue to carry its kilowatt load, but at a different power factor.

Figure 5. Electronic voltage regulator schematic diagram



Rototrol Main Exciter

The Rototrol with its two stages of amplification can be built with large power output capabilities while the control field energy requirements are sufficiently small to be supplied by ordinary instrument transformers. Also, since the Rototrol is a high-speed machine with air-gap dimensions the same as any other d-c machine, it can be direct-connected to the shaft of the turbine generator. The direct-connected main-exciter Rototrol is a step toward simplification of turbine-generator construction, operation and maintenance by completely eliminating the pilot exciter. The impedance-type voltage regulator automatic control unit and the potential unit are exactly the same as those used with the Rototrol buck-boost pilot exciter. Performance of the Rototrol main exciter is comparable to that obtained with conventional excitation systems. Complete details are presented in a companion paper.[3]

Hydroelectric Generators

At present it is not practical to direct-connect the Rototrol to the water-wheel generator shaft, because of the multiplicity of speeds and sizes. The Rototrol buck-boost pilot exciter, therefore, must be driven by a small motor which introduces the problem of a reliable power supply for the driving motor. The three-field main exciter excitation system in Figure 2 is readily adaptable to slow-speed generators and is shown in Figure 3. During normal operation of the a-c generator, the excitation system will function as the three-field main exciter scheme described previously. The self-excited shunt field provides the base excitation while the Rototrol provides buck-boost action to compensate for changes in generator voltage.

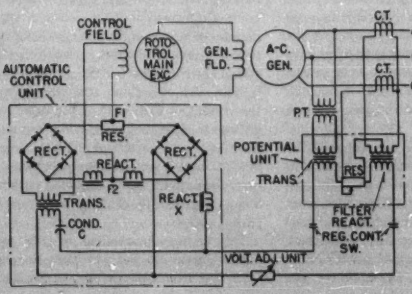
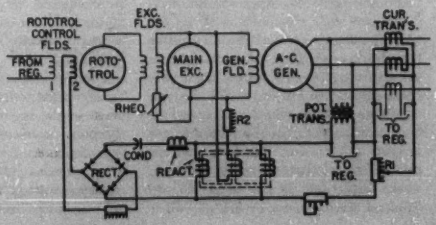


Figure 4 (left). Voltage regulator static-network automatic control, potential and voltage adjusting units

Figure 6. Variable minimum-excitation control unit



During start-up of the generator when no outside source of supply is available for driving the Rototrol motor-generator set, the main exciter is operated as a self-excited machine, and will provide excitation for the main generator. As soon as a-c voltage is available, the Rototrol may be started and the voltage regulator placed in service, or operation can be continued under hand control with the operator controlling excitation with the self-excited shunt field rheostat.

During short circuits on the a-c system, the excitation system must be capable of supplying full excitation to the generator field. With the system shown in Figure 3, this is accomplished by building sufficient inertia into the Rototrol motor-generator set to carry it through such disturbances with very little change in speed even under severe forcing conditions.

An alternative answer is provided by making the Rototrol motor-generator set a three-unit set with an a-c and d-c driving motor. During normal operation the set would be driven by the a-c motor with the d-c motor floating on the station battery and acting as a d-c generator. During an a-c system disturbance the d-c machine would automatically take over the driving function, when the a-c voltage dropped. Upon restoration of the a-c voltage, the a-c motor would again drive the set.

A separate source of a-c energy, entirely independent of the a-c generator, might also be used to drive the Rototrol. Obviously such a supply must be extremely reliable unless automatic transfer schemes are provided. Any trouble on the separate a-c source, which might be a house-service generator, would disturb operation of the Rototrol excitation system causing a disturbance on the main a-c system. The most reliable system is believed to be that shown in Figure 3, where disturbance in the supply to the Rototrol driving motor occurs only when there is a disturbance on the main a-c system, and sufficient inertia in the Rototrol motor-generator set allows it to ride through the disturbance without trouble.

Synchronous Condensers

Either the single- or multiple-field main exciter Rototrol excitation system may be used with synchronous condensers. As is the case with hydroelectric generators, however, the Rototrol must be driven by a separate motor at a higher speed. Where the main exciter is separately driven as in one standard scheme now used, the Rototrol can be mounted on the main-exciter shaft as a conventional pilot exciter. The three-field main exciter is normally used with synchronous condensers because of its advantages over the single-field scheme. The main-exciter stabilizing field is necessary because of the wide range of operating voltage required of the main exciter. The single-field main exciter scheme is, however, equally applicable and, as pointed out before, comparable in performance to existing pilot exciter schemes.

Since an a-c supply must be available for starting the synchronous condenser in the usual case, the source of driving energy for the Rototrol motor-generator set is no problem. This is properly chosen as the a-c system to which the condenser is to be connected. Sufficient inertia in the motor-generator set enables it to ride through system disturbances without difficulty.

The electronic type of voltage regulator automatic control unit may be used in place of the impedance type with synchronous condensers. The electronic type functions by comparing the condenser terminal voltage with a constant reference voltage which is generated in the regulator itself. Deviation of the condenser voltage from the regulated value causes corrective action in the Rototrol control field. Standard types of electronic tubes having a high degree of reliability are used in the electronic regulator. The tubes are worked below normal rating in order to provide long life.

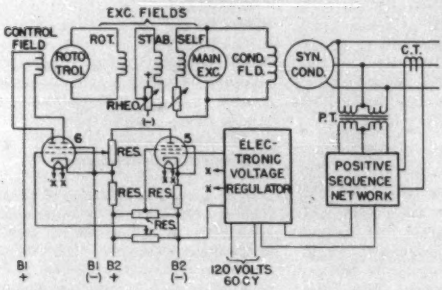


Figure 7. Electronic generator voltage regulator with Rototrol buck-boost exciter

Static Regulator for Rototrol Excitation System

A static-network, impedance-type, voltage-regulating equipment can be made up of a static network which includes a voltage-responsive circuit, and a self-contained reference.[4] The connections to the regulator component parts are shown in Figure 4. A-c voltage and current are connected to a voltage-regulator potential-unit which includes resistance and a filter reactor. This circuit is used to provide a positive-sequence component of voltage for the voltage responsive circuit which includes the capacitor and reactor impedance. The voltage-adjusting unit is connected between the potential unit and the voltage-regulator automatic-control unit. The voltage-adjusting unit provides a means of increasing or decreasing the resistance in series with the automatic control unit. This causes the regulator to regulate, when resistance is added in the circuit, for a higher voltage at the generator terminals: and when resistance is removed from the circuit for a lower voltage at the generator terminals.

The a-c circuit from the potential unit feeds directly the saturating reactor X, and the capacitor C circuits. The output of the reactor circuit and the output of the capacitor circuit are rectified by single-phase full-wave dry-type rectifiers. The rectifiers are then connected with additive relation in series through a resistor and smoothing reactors. The control field of the Rototrol is connected between a tap on the resistor and the opposite side of the rectifier circuit.

When the a-c voltage is normal at the point of intersection of the characteristic-impedance curves, current of equal magnitude flows in the saturating-reactor circuit and in the condenser circuit, through the resistor and smoothing reactors. Since the current in each path is the same no difference current flows in the circuit, $F1$-$F2$, of the Rototrol control field. Should the a-c voltage become low however, the current in the capacitor path is large compared to that in the reactor path.

This causes the rectifier in the capacitor circuit to put out more current than that from the reactor rectifier and

this current flows with positive polarity on *F1*, through the control field of the Rototrol. This causes the Rototrol to raise its voltage until the a-c voltage is returned to normal at which time the current flow between *F1* and *F2* approaches zero.

Should the a-c voltage become high instead of low, the current in the reactor path is large compared to the current in the capacitor path and the rectifier in the circuit of the reactor puts out more current than the capacitor rectifier and in this case the difference current is positive at *F2* flowing through the Rototrol control field. This causes the Rototrol to lower voltage until again the current in the control field circuit approaches zero.

Electronic-Type Voltage Regulator

The electronic-type voltage regulator is shown in Figure 5. This type of regulator is responsive to the average 3-phase or positive-sequence voltage by using two potential transformers and a current transformer for the average 3-phase voltage, or two potential transformers for the positive-sequence filter network. This regulator also has a self-contained reference in the form of a pentode tube. The reference voltage is produced by operating the pentode tube 1 with constant control and screen-grid voltages of the proper value. A constant plate-current characteristic is thus obtained, which is entirely independent of reasonable variations in the plate voltage. The reference voltage is obtained from the drop across resistor *R1* which carries the constant plate current. The control-grid voltage is held constant by applying the drop across the cathode resistor *R4* through which the plate current also passes. The screen-grid voltage is held at a definite value by means of the voltage-regulating tube 2. This combination provides a circuit using electronic tubes and resistors, no battery, which requires only a source of reliable a-c voltage of small capacity for the tube circuits.

The voltage-sensitive part of the circuit is a filament-controlled diode high-vacuum tube. The voltage to be controlled, either a-c or rectified from a-c, can be fed into the filament of this tube and a d-c voltage, proportional to the rms value of the a-c voltage is thus obtained from the loading resistor. This voltage is balanced against the reference voltage and the differential voltage between the two is fed into a suitable amplifier. The output of the amplifier circuit can be used to energize the control field of a Rototrol pilot exciter.

Compensation for Equalizing Loads Between A-C Machines

In each of the regulators described in the foregoing a compensating characteristic can be introduced by means of a-c machine current to cause the regulator to have a drooping or a rising characteristic with increase in reactive load.

For the impedance-type regulator a drooping or rising characteristic for the regulator voltage is obtained by means of a resistor in the regulator potential unit. By referring to Figure 4, it will be noted that potential is taken from phases 1 and 2 of the a-c machine, and current from phase 3 flows through the resistor in the potential unit. With this connection the voltage and current are in the proper vector relation so that the voltage developed in the resistor with increase in lagging reactive current can be used to provide a droop in the a-c voltage which the regulator will control. This can be used in individual regulators for a-c machines operating in parallel to make them share the reactive load. Similarly, a rising characteristic can be obtained to compensate for line drop.

With the electronic-type regulator suitable vector relation between a-c current and voltage can be secured to give a drooping or a rising characteristic to the regulated voltage.

Excitation Limits

For turbine driven a-c generators in central station service it is desirable to have the generator field excitation maintained high enough so that the a-c generator will be stable in case of system disturbance or in case of suddenly applied additional kilowatt load. The exciter voltage under regulator control should be prevented from lowering to the point of reversal.

For large synchronous condensers it is desirable to limit the a-c current with the condenser overexcited to a value within the rating of the machine. The magnitude of lagging reactive kilovolt-ampere condenser underexcited can be controlled by means of a lower limit of excitation. Circuits of the static-network type, or electronic type, are available for such control.

Figure 6 shows a minimum-excitation control circuit, the effect of which varies with kilowatt load on the a-c generator. As the kilowatt load increases a higher level of generator field excitation is required, this increase being approximately proportional to load. Connections of a-c voltage and current are made to secure the proper voltage vector relation so that the current flowing through resistor *R1* provides a voltage the magnitude of which varies with the kilowatt load. Exciter voltage is taken as an indication of generator field excitation. This voltage is applied to the d-c current winding of a saturating reactor which together with resistance and reactance in the circuit, controls the output of the rectifier and therefore the energy in Rototrol control field 2. The relation between excitation and kilowatt load may be adjusted by means of the resistors *R1* and *R2*.

The Rototrol has two control fields, one energized by the voltage regulator and the other energized by the minimum-excitation control circuit. Should the a-c system voltage rise thereby causing the regulator to lower excitation, and should this lowering of excitation call for exciter voltage below the value which has been predetermined for the kilowatt load which the a-c machine is carrying at that time, the decreasing exciter voltage lowers the saturation of the three-core reactor, thereby causing the a-c voltage to rise on the rectifier which puts current into Rototrol control field 2. This is in a direction to raise the main exciter voltage until this circuit is satisfied, opposing and overcoming, if necessary, the regulator input into Rototrol control field 1. In this way the excitation of the a-c generator is maintained with relation to the kilowatt load so as to follow a predetermined characteristic.

Similar circuits can be provided to limit the a-c current which a synchronous condenser will put out, although its automatic voltage regulator may call for increased excitation.

For an electronic-type voltage regulator circuits using tubes can be provided both for the control of exciter voltage and for limiting features. Figure 7 shows the circuits used with an electronic voltage regulator. This type of regulator controls the Rototrol by means of center-tapped field windings, one part being used for raise action and the other for lowering.

Electronic circuits for limitation of minimum exciter voltage can be added to those shown in Figure 7. The automatic voltage regulator may call for exciter voltage so low that it might permit the condenser to fall out of step. In such a case the limiting circuit provides for energizing Rototrol control field 2 if the exciter voltage drops below a predetermined point. It will then raise excitation even though the regulator may at the same time call for lowering the excitation.

An electronic circuit is also available

for limiting a-c overcurrent. This circuit is arranged to put energy into Rototrol control field 2, but in the opposite direction to the minimum exciter-voltage circuit. Should the a-c current increase above the predetermined value, current will flow in Rototrol control field 2 in the direction to lower excitation. This continues until the a-c overcurrent is reduced and the circuit is satisfied.

Conclusions

The Rototrol amplifier and its associated impedance, or electronic-type voltage regulator and limiting devices form a type of excitation system in which radical departure is made from previous excitation methods. Several advantages are gained over conventional methods with no sacrifice in reliability. Since the Rototrol is a machine of normal design, it can be direct-connected to the shaft of a turbine generator, thus maintaining the advantages of this type of drive.

When the Rototrol pilot exciter is used to energize the single field of a main exciter, it provides for automatic control by means of a regulator without moving parts and manual control by means of a small control unit, thus eliminating a large motor-operated exciter field rheostat and associated contactors. The Rototrol pilot exciter has a mechanical and electrical reliability equivalent to that of conventional pilot exciters, which have been used successfully for many years.

The Rototrol buck-boost pilot exciter and static-type voltage regulator offer advantages over other forms of excitation systems. When used with a self-excited exciter having a field energized by the Rototrol alone, the a-c machine can be operated with or without the Rototrol in service. In the latter case, the self-excited field provides excitation, being controlled manually by a main exciter field rheostat.

The buck-boost pilot exciter is also adaptable to use with hydroelectric generators and synchronous condensers. In both these cases the pilot exciter must be separately driven, which is not objectionable with synchronous condensers since a-c power must be available to run the condenser. With a waterwheel generator, the separately-driven pilot exciter may not be considered as reliable as a direct-connected pilot exciter. However, the three-field main exciter provides build-up of a-c voltage by means of its self-excited field, thus permitting starting of the station with no separate a-c supply. Sufficient inertia in the motor-driven Rototrol buck-boost pilot exciter enables it to carry through a-c system disturbances.

The Rototrol main exciter has the advantage of removing the need for a pilot exciter of any kind, and also the need for a motor-operated main exciter field rheostat. The same type of static voltage regulator as used with the Rototrol pilot exciter is employed with the Rototrol main exciter. This system is described in detail in a companion paper.[3]

References

1. The Multistage Rototrol, **M. M. Liwschitz.** AIEE Transactions, volume 66, 1947, pages 564–8.

2. Two-Stage Rototrol for Low-Energy Regulating Systems, **A. W. Kimball.** AIEE Transactions, volume 66, 1947, pages 1507–11.

3. Main Exciter Rototrol Excitation for Turbine Generators, **C. Lynn, C. E. Valentine.** AIEE Transactions, volume 67, 1948, pages 535–9.

4. Static Voltage Regulator for Rototrol Exciter, **E. L. Harder, C. E. Valentine.** AIEE Transactions, volume 64, August 1945, pages 601–06.

Discussion

E. C. Starr (Oregon State College, Corvallis, Ore.): We are particularly pleased to see the development and practical application of excitation systems that are very fast, simple, require little maintenance and contain a minimum of moving parts. The static-type regulator with zero dead band and the rotating d-c amplifier have contributed much toward this end. With further refinements, including reduction of exciter field time constants, increase in ceiling voltages and lamination of stator structures, it should be possible and practical to so force the fields of large generators during system disturbances as to produce definite compensating changes in the "voltage behind transient reactance" during system swings having periods of 30 cycles or less. In that case transient stability would be improved and proper account of internal voltage changes should be taken in network-analyzer swing-curve studies.

We should appreciate the authors' comments on the possible extent and timing of this voltage change such as might be produced practically in large synchronous generators and condensers.

J. E. Barkle and C. E. Valentine: In reply to the discussion by E. C. Starr. The authors wish to state that they are also much interested in the possibilities along the lines suggested by him. The best answer so far as forcing the field of the large ac machine is concerned, lies in the use of an all-electronic excitation system. Next to this is the use of a rotating amplifier-type main exciter, designed to have a high rate of response.

A main exciter of conventional construction and a buck-boost rotating amplifier have inherently less fast response over-all, than a main exciter Rototrol. To comment more completely and accurately, as suggested, taking into account the many variables involved would require considerable time and study. Undoubtedly, improvements over past performance can be made as experience with the new excitation systems increases.

Main Exciter Rototrol Excitation For Turbine Generators

C. LYNN
MEMBER AIEE

C. E. VALENTINE
MEMBER AIEE

WITH the increase in size of turbine generator units and the accompanying higher steam pressures and higher temperatures, these generating equipments have become more of the unit type. Boilers, power house auxiliaries, turbines, generators, exciters and regulators function as a unit for an individual equipment. Owing to the more efficient operation of these units, it is desirable to keep them continually on the line. Shutdown periods that previously were considered normal for boiler and station auxiliaries, which in the past provided time for brush replacement and other usual maintenance operations on exciters, are no longer permitted, as these equipments are now designed to operate over very much longer periods between shutdowns. Excitation systems, therefore, have also been redesigned from the standpoint of continuity of operation.

The use of Rototrol pilot exciters, direct connected for new installations, and separately motor driven for older installations, where direct-connected units cannot be installed, using static networks for voltage regulation, has eliminated sources of possible shutdowns and at the same time has improved the over-all excitation operation. A companion paper[1] covers Rototrol excitation systems, in general, and mentions main exciter Rototrols for turbine generators.

The surest way to eliminate failure of any piece of apparatus is to either eliminate the apparatus or modify it to make it simpler, so that there is inherently less chance of failure. In main exciter Rototrol excitation for turbine generators, not only is the regulator with contacts and contactors eliminated, but also the pilot exciter. In this type of equipment, therefore, we accomplish with one main exciter, complete excitation, stability and regulation in the one unit, with only the addition of a static voltage regulating network. The latter being static has, of course, no moving parts. Our purpose is accomplished in this one exciter without the use of any additional brushes, short circuited windings or connections, or other constructions that are critical in adjustment or have additional potential possibilities of trouble. Since the amplification is produced in the main exciter itself, it is built as a two-stage rotating amplifier instead of a single-stage unit, in order to require only a nominal amount of control energy from the static network. The principle and operation of the two-stage Rototrol has already been covered in the AIEE papers.[2,3] Rototrols of the two-stage type previous to this application for turbo-exciter excitation, have been built in capacities up to 15 kw at 1,200 rpm.

Main Exciter Rototrol

The 200-kw 250-volt 3,600-rpm main exciter Rototrol, is shown on the test floor in Figure 1, with cover removed, built for a large western utility company, for the excitation of a 60,000-kw 13,800-volt 85 per cent power factor, 3-phase 60-cycle 3,600-rpm turbogenerator. This is the first unit of this type to be built, and it will be necessary to gain field experience before additional machines are made generally available.

Figure 2 shows the unit with cover in place. In general, the unit follows the latest type of Westinghouse direct-connected turbo exciter construction with "rollaway" type of cover. This gives an enclosed machine for normal operation, having far greater accessibility for brush replacement and inspection, with the cover removed, than any open-type d-c machine of similar size built heretofore.

Figure 3 is a close-up of the current collecting parts, and Figure 4 shows the brush holder. This is a double-type brush holder, providing brush replacement using standard shunted brushes, without the use of any tools around the machine, while the machine is in normal operation. Shutdowns, therefore, are not necessary for brush replacement. The brushes are normal in number and arrangement, the same as on a conventional type 200-kw 3,600-rpm exciter. A depressed type of bedplate is used, giving a large open space underneath the commutator for ease of brush replacement. No structural members other than the brush holder rods lie in the space surrounding the commutator. Shrink rings are used on account of the high speed involved and the shrink ring commutator is the Westinghouse floating type with means provided to take care of radial and axial expansion caused by temperature changes and stresses of the rotation. The armature winding is of the conventional lap type with cross connections omitted. The openings between the commutator necks are filled with insulating material and the armature winding is packed behind the necks and under the front coil extensions to eliminate creepage paths which might cause low insulation resistance due to flyash in the air. The only exposed live-creeping surfaces on the armature are the insulation adjacent to the shrink rings and the insulation adjacent to the bands on the front armature coil extensions, both of which can be cleaned with compressed air, with the unit in operation, or wiped by hand under slow roll condition, if found necessary.

The stator is of normal design, with the exception of modified brush holder cross

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C. LYNN is with the direct current engineering department, and C. E. VALENTINE is with the regulator section, switchgear engineering department, of the Westinghouse Electric Corporation, East Pittsburgh, Pa.

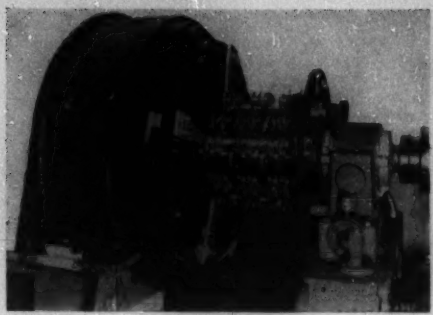

Figure 1. Main exciter Rototrol, 200-kw 250-volt 3,600-rpm. Set up for factory test

Figure 2. Main exciter Rototrol, 200-kw 250-volt 3,600-rpm. With enclosing cover in place

connections and additional windings on the fields as required to obtain two stages of amplification in one unit. In order to minimize the effects of hysteresis, materially eliminate eddy current effects and provide faster speed of response, a laminated type frame is used, providing a completely laminated structure for the main field flux. The commutating pole windings and the series field windings, as well as the shunt field windings are also insulated throughout the full length to eliminate creepage paths. Pedestal-type bearings are used since these require no tying members to the frame and permit maximum accessibility, particularly in regard to oil seals around the shaft, thus minimizing the possibility of oil leakage.

The exciter is driven from the commutator end by a fast flexible coupling connecting it to a stub shaft on the end of the a-c generator. A double type fan is mounted on the end of the turbine generator shaft and provides separate ventilation systems for the a-c exciter and the a-c generator collector rings. Baffles or bulkheads, built into the machine and projecting inwardly from the cover, with rubber seals provide partitions in the exciter housing for directing the ventilating air in the paths for most efficient cooling. The ventilating air flow is from the rear of the machine towards the commutator and fan, so that no carbon dust from worn brushes can be drawn into the machine windings. An end bell at the rear edge of the frame directs the air into the rotor and field coils in the correct proportions. The baffle at the front end of the commutator causes the ventilating air to pass over the commutator, thus cooling it and the brushes before passing into the fan. Air for ventilating the collector rings is carried from the rear of the exciter in hollow tubes through the base, to the a-c generator field collector rings and brushes. The fan blades on the collector side of the exciter fan provide air circulation in the collector ring compartment. Suitable filters are used at the air entrance into the exciter unit. The air discharge is directly below the fan, located at the end of the a-c generator shaft. Suitable inspection openings are provided in the cover, with interior lights provided for illumination, so that operation can be observed with the cover in place. The cover is not attached to the exciter but is mounted on rollers which are brought into play by movement of a handle at the end of the cover. When this handle is pulled rollers are depressed so that the complete cover can be rolled away from the exciter, uncovering successively the a-c collector rings and brushes, and the commutator and its brushes. This can, of course, be done with the unit in normal operation. This requires a cover movement of less than half of its length. As a turbine generator exciter, the design is made for maximum accessibility and continuity of service.

Regulation of Voltage

The regulation of the terminal voltage of the Rototrol main exciter and of the a-c machine which it excites, requires a voltage-responsive device or circuit which can transmit the proper intelligence to the control field of the Rototrol. The multistage Rototrol exciter or d-c generator differs from the conventional d-c generator or exciter in the magnitude of its amplification. Since it is designed with the object of providing maximum amplification in output terminal voltage for a small change in the control field input energy, it becomes possible to control this type of machine with a small amount of energy from a voltage-responsive regulating device or circuit.

Any one of a number of regulating devices or circuits can be used. Electromechanical devices, in general, have the advantage of having a convenient fixed reference such as the tension of a coiled spring, but have had, until within recent years, the disadvantage of moving and wearing parts. Direct-acting rheostatic type regulators which use complete spring mounting for the moving parts[4] have overcome essentially the disadvantages of these wearing parts. This direct-acting type of regulator can be used to control the Rototrol by putting the control field in a Wheatstone-bridge circuit.

Regulating action, similar to that obtained with electromechanical devices, can, however, be secured by means of static circuits with or without electronic tubes. Electronic-tube circuits can be made extremely sensitive, can have high amplification, and with suitable power source can provide relatively large output energy for a very small amount of input intelligence. Electronic tube circuits have many interesting possibilities for voltage-responsive and other regulating

Figure 3. Close-up view of type of current collecting parts used on 200-kw 250-volt 3,600-rpm main exciter Rototrol

Figure 4. Brush holder used on main exciter Rototrol permitting brush replacement without tools

functions. Electronic input or measuring circuits have the advantage in general of requiring a small amount of energy from potential or current transformers on account of the sensitivity and amplification within the tube or tubes. A basic requirement for this type of circuit, however, is a reliable source of energy for the amplifier and rectifier tubes. Electronic tubes require some attention over a period of months or years of operation, to check operating characteristics and to replace when necessary.

The static network circuit[4] without electronic tubes can be made up of component parts which do not require replacement. These parts include transformers, reactors, condensers, resistors, and dry-type rectifiers. The input energy for this type of circuit is in general higher than for electronic circuits, and may be supplemented by separate-source energy. Network circuits of this kind were developed and used for various purposes during World War II. Further development of such circuits has continued since the war.

In the voltage-responsive static circuit, a self-contained reference is desirable, although a separate source of energy such as a battery or constant-potential device may be used. This reference takes the place of the spring in the electro-mechanical type regulator. For this purpose the voltage and current characteristics of differing types of impedance may be employed. The circuit described in this paper uses a reference which consists of the intersection of a non-linear impedance characteristic obtained from a reactor, and a linear impedance characteristic obtained from a condenser. The voltage-responsive regulating circuit is satisfied or "balanced" at the point of intersection of the impedance characteristics. Any deviation from the level of voltage for which the intersection is designed will result in a current difference in the static network circuits which is used to provide energy of a given polarity or of the opposite polarity to energize the Rototrol control field.

Figure 5 illustrates the intersecting impedance characteristics. The curve *OPX*, a nonlinear curve, shows the relation of a-c applied voltage E and the current I flowing through the saturated reactor. The curve *OPC*, a straight-line curve, shows the relation between the applied a-c voltage E and the current I through the capacitor. The value of voltage chosen for the intersection of the impedance characteristics corresponds to a value somewhat less than the normal voltage of the a-c generator, reduced through potential transformers. A suitable range of adjustment for the level of voltage which the generator will hold is obtained by means of variable resistance connected in series with the circuit which gives the fixed reference level. The switchboard operator turns his voltage-level adjusting rheostat, thereby varying the resistance in the circuit, to adjust the level of voltage which it is desired that the regulator shall hold.

An excitation system with the static regulator network, is shown in Figure 6. The voltage regulator automatic control unit includes the saturating reactor and capacitor circuit. Other component parts are the voltage regulator potential unit and the voltage adjusting unit. In the potential unit, a-c potential and current are used to provide positive-sequence voltage for the automatic control unit, and adjustment for equalizing the reactive load between a-c machines which operate in parallel. A regulator control switch is used to cut the regulator into or out of service. A manual control unit is provided to control the Rototrol exciter manually when the voltage regulator is not in service.

Figure 7 shows the internal connections for the automatic control unit and for the potential unit. For the automatic control unit this shows the input a-c voltage, feeding direct to the capacitor circuit C, and to the reactor circuit X. These circuits each energize a dry-type rectifier. The output of the rectifiers is connected in a series additive relation through a suitable tapped resistor. The Rototrol control field is connected from the center of the resistor to the opposite side of the rectifier energized circuit.

When the intersecting impedance circuit is satisfied with voltage at the intersection of its electrical characteristics, the current and voltage output of each rectifier is the same. No difference current appears in the Rototrol control field. Should the a-c voltage drop below normal, the current in the reactor circuit becomes less than that in the capacitor circuit thus causing a current to flow in the Rototrol control field with the polarity of the capacitor rectifier. The flow of the current is in the raise direction to cause the Rototrol to increase its output, thus restoring the a-c generator voltage to normal.

Should the a-c voltage rise above normal it will be noted that the current in the reactor circuit becomes greater than that in the capacitor circuit, this resulting in a difference current in the output of the rectifiers, the polarity being that of the rectifier in the reactor circuit. This

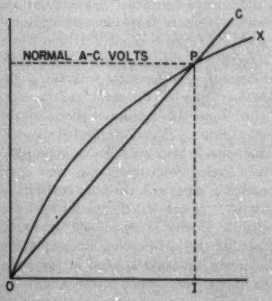

Figure 5. Static impedance type voltage regulator—linear and nonlinear impedance characteristics

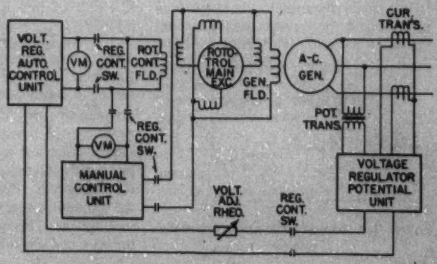

Figure 6. Excitation system with Rototrol main exciter and impedance type voltage regulator

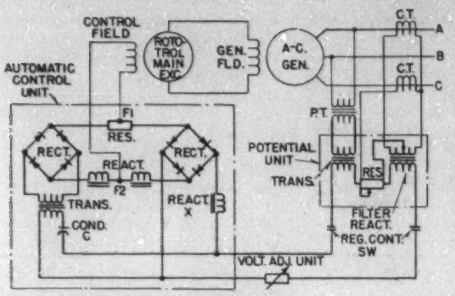

Figure 7. Voltage regulator static network automatic control, potential, and voltage adjusting units

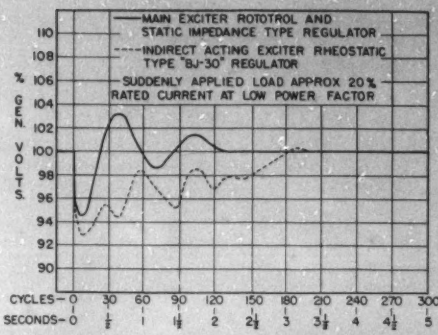

Figure 9. Comparison of voltage dip and recovery time main exciter Rototrol regulating system and conventional main and pilot exciter with contact-making regulator

polarity is opposite to that resulting when the a-c voltage is low, and the Rototrol control field is thus energized in the lower direction to decrease its output voltage and that of the a-c generator to return the a-c voltage to the normal value.

The manual control unit includes a bridge circuit and rheostat which is energized from the output of the Rototrol. The circuit includes resistance and a dry-type rectifier arranged for connection to the control field as shown in Figure 8. For a given setting of the manually operated rheostat, the circuit is self-regulating to control the Rototrol output voltage, maintaining it at a predetermined value. Turning the rheostat to a new position raises or lowers the Rototrol output voltage as required, and thereby controls the generator a-c terminal voltage.

The regulator control switch is used to change from manual to regulator control and vice versa. This switch has four positions; manual, regulator test, manual test and regulator. When changing from manual to regulator control, the switch is turned to the regulator test position and the automatic control unit output is adjusted by means of the voltage adjusting unit and voltmeter to match that of the manual control unit. The switch is then turned to the regulating position and the regulator takes control of the a-c voltage. When changing from regulator to manual control, the switch is turned to the manual test position and the manual unit output is adjusted by means of its rheostat and voltmeter to match that of automatic control unit. The switch is then turned to the manual position. When the manual control unit is being used, should the exciter voltage start to change from the setting determined by the position of the manual control rheostat, the bridge circuit in this unit energizes the Rototrol control field in the proper direction to return the Rototrol exciter voltage to the predetermined manual setting.

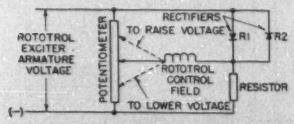

Figure 8. Manual control unit—schematic diagram

The large main exciter Rototrol has been tested and shipped, and the unit is now being installed. The following factory tests were obtained with the exciter and its static voltage regulation network, loading the unit on the fields of a large a-c generator comparable in characteristics to that of the generator which it will excite. Companion tests were made on a similar size unit of conventional construction, using a main exciter, a pilot exciter, and a modern exciter rheostatic-type voltage regulator.

Factory Tests

The a-c generator was a 20,000-kva 6,600-volt 3-phase 60-cycle 600-rpm laboratory test machine. The excitation requirements and time constant of this generator are comparable to those of the 70,500-kva 3,600-rpm turbine generator which will be excited by this main exciter Rototrol. The 250-volt 3,600-rpm main exciter Rototrol was connected to energize directly the field windings of this generator. A 3-phase reactor load was suddenly applied, causing about 20 per cent rated a-c generator amperes to flow in the load circuit. Figure 9 shows the voltage dip and recovery as plotted from an oscillogram. This voltage dip was $5^1/_2$ per cent, and was followed by a return to normal voltage in 23 cycles, and final recovery in about two seconds. It should be noted that the static-network regulator and machine adjustments in general, which provide for recovery in the shortest possible time are accompanied by a greater overswing and underswing of the regulated voltage, compared to the use of a slower rate of recovery. With a slower rate of recovery, the swinging of the voltage above and below the regulated value can be decreased. The available test facilities did not permit applying loads of greater magnitude. The test as made, however, indicates relatively rapid response of the Rototrol voltage, with the required forcing ability to return the a-c generator voltage to normal in spite of relatively long time delay inherent in the a-c generator field circuit.

The dashed curve in Figure 9 shows typical performance of an exciter rheostatic voltage regulating system, using conventional 250-volt 3,600-rpm main and pilot exciters. For this test a 250-volt 3,600-rpm conventional type main exciter, with a directly connected conventional pilot exciter, was connected to energize directly the field windings of this same laboratory a-c generator with the shunt field winding of the main exciter energized from the constant voltage of the pilot exciter. A motor-operated rheostat and suitable blocks of field-forcing-up and field-forcing-down resistance were connected in series with the fields of the main exciter. In this test the reactor load was slightly greater in magnitude than that used for the main exciter Rototrol tests, and was suddenly applied in the same manner. The dashed curve shows an

initial voltage dip of 7 per cent and a recovery time of three seconds.

With this type of regulator it should be noted that the rate of recovery of the a-c voltage depends on the contact gap and timing adjustment which is used with the high speed contactor, and also upon the speed of the motor-operated rheostat. The dashed curve of Figure 9 shows an adjustment of the regulator contacts which energize the high speed contactors of plus or minus $2^1/_2$ per cent. The normal-response contacts which control the motor-operated rheostat, were set at plus or minus one-half of 1 per cent.

The voltage dip of the recovery of the exciter rheostatic regulator can be improved by setting the contacts which control the high-speed contactors to a small percentage value. The practice of different operating companies varies considerably in this respect. Where the generator load is likely to fluctuate widely or violently, the contact gap setting for the regulator quick-response contacts may be only a little greater than that of the normal-response contacts. For the majority of public utility system regulators, however, the setting of the quick-response contacts is usually greater, for example, between plus or minus 3 and plus or minus 5 per cent. With this setting the high speed contactors operate very infrequently, providing mainly for voltage regulator high speed action, which takes place only in case of serious a-c system disturbances. For the usual system load changes the normal-response contacts regulate the a-c voltage by controlling the motor-operated rheostat alone.

The main exciter Rototrol has the advantage of combining in one rotating machine the amplification necessary for control directly by the voltage regulator and ample power output for connection directly to the field of the turbine driven generator. Since the Rototrol is series tuned it utilizes the inherent transient characteristic of the a-c generator field wherein current increases or decreases momentarily under the condition of sudden application or removal, respectively, of load. This amounts to an anticipatory action as far as the regulator is concerned because the Rototrol starts to change its output prior to action by the regulator in the Rototrol control field circuit.

The voltage regulating system described has the advantage of a regulator without moving parts, without contactors, and requiring no large motor-operated main-exciter field rheostat. The over-all performance of the regulator, Rototrol exciter and the a-c machine, shows marked improvement in voltage dip and recovery time. The quicker recovery of the a-c voltage and even some over-swing, is in a direction to greatly improve stability during the transient period. Such a system also eliminates the use of a pilot exciter without effecting the stability of the main exciter. The Rototrol main exciter is connected to and driven by the turbine generator, and is controlled directly by the regulating equipment, simplifying the regulating and excitation system.

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Discussion

Alexander Dovjikov (Bonneville Power Administration, Portland, Ore.): As the control field provides only the necessary stabilizing effect, holding the rototrol voltage at a constant value, and all the excitation requirements of the pilot exciter are provided by the self-energized "series" field, we see in this development a feature in which we have been interested for a long time, a feature which European designers took advantage of long ago by using and advocating further use of series exciters.[1,2] The reason for a series winding, as recognized by Barkle and Valentine, is that the series-tuned rototrol has the advantage of utilizing the induced transient current in the generator field and in the main exciter series field winding due to a-c short circuit or the sudden application or removal of load. We were very interested to see that the machine not only has a shunt winding but a series winding as well, and that

"the laminated type frame is used, providing a completely laminated structure for the main field flux."

It should be emphasized that for the fastest response the magnetic circuit of the machine must be laminated, and we are glad to see this principle incorporated in the design of this exciter.

We are also very interested in Figure 9 of the paper representing the voltage-time curve of a Rototrol regulating system with approximately 20 per cent of rated current at low power factor suddenly applied. With this type of load applied, it naturally takes time to build a quite considerable additional air-gap flux required by the load, and the indicated speed is fairly promising for application when fast increase in internal electromotive force is desired, as for example, during swings. From a stability point of view, we would like to see the same kind of graph (with space 0–30 cycles considerably expanded and with more details) for a typical large generator with excitation corresponding to the full load, the fault applied and then removed in approximately three to five cycles, the conditions at which it is desirable not only to maintain the air-gap flux, but even to increase it during the subsequent swing of the synchronous machines.

References

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2. Long-Distance Transmission of Energy and the Artificial Stabilization of Alternating-Current Systems, G. Darrieus. Presented at the International Conference on Large High Voltage Electric Systems (Paris, France), 1946.

Frank V. Smith (Sargent and Lundy, Chicago, Ill.): The authors have presented a paper of considerable interest. The prospect of eliminating the pilot exciter on large turbine generators is one that will be welcomed by power engineers. This small machine on the end of the shaft has been a matter of concern to operators who fear the outage of much needed capacity because of trouble with the pilot exciter. Its existence has been a factor in the use of separately driven exciters in some cases although this solution is not altogether satisfactory because of the added complications of motor and control and the requirements of high inertia to hold the exciter voltage up during a-c disturbances. The main exciter Rototrol described here has yet to be proven in service. Applications of the static regulator and the Rototrol to replace the pilot exciter have been made only recently and very few of these are yet in operation. Power engineers will look forward to the record of experience in the field.

The Effect of Coulomb Friction on the Performance of Servomechanisms

G. D. McCANN
MEMBER AIEE

F. C. LINDVALL
FELLOW AIEE

C. H. WILTS
NONMEMBER AIEE

THE EFFECT of coulomb friction on the performance of three basic types of speed-regulating and position-regulating systems has been studied by means of the electric analog computer. The analysis presented here shows the effect of such friction forces on servo performance including speed of response, damping, and control error. The amount of coulomb friction is specified in terms of a dimensionless parameter called "the friction ratio" that relates the friction force or torque to the disturbance. The equation for the friction ratio varies with the type of system and disturbance and must be specifically defined in each case.

It was found that the presence of coulomb friction in the follow-up motion portion of a position-follow servo always increases damping, but also increases the error and decreases the speed of response. However, if the coulomb friction is present in a portion of a servomechanism containing a degree of freedom different from that to be specifically regulated, its effect is usually one of destabilization. In the case of a simple speed governor described by a cubic equation, coulomb friction in the governor mechanism will cause destabilization and poor performance in governors otherwise made stable by appreciable viscous damping. However, in a limited region of system parameters characterized by low viscous damping in the governor, it can increase stability. Appreciable coulomb friction in the control surface circuit of the auto pilot selected for study was found to be undesirable in the practical range of the system parameters. Damping of an otherwise unstable auto pilot can be achieved with coulomb friction but poor over-all performance is indicated. Under certain conditions the destabilizing effect of coulomb friction can be overcome by increased damping from both the air frame position and the control surface position.

The variable effects of coulomb friction and the conditions under which these effects become appreciable are discussed.

Although coulomb friction is frequently found to be an important factor in self-regulating systems, its general effects on servo performance have not been adequately analyzed. Its discontinuous nature prevents detailed study by conventional mathematics. However, it is a relatively simple function to represent by electrical analogy, and the electric analog computer is well adapted to a complete study of its effects. The California Institute of Technology computer[1] has now been used for a general exploratory analysis of the effect of coulomb friction in three basic types of servomechanisms: position follow-up servos, speed governors and auto pilots.

Electrical Analogy for Coulomb Friction

Coulomb friction is defined as a constant force or torque, independent of velocity, that always opposes motion. When the sum of all other forces on an object is less than the friction force, grabbing occurs. These conditions are satisfied by the circuit of Figure 1, when the force-voltage analogy is used. Examination of this circuit shows that if the back impedance of the rectifiers is assumed to be infinite, current cannot flow through either branch of the circuit until a voltage opposite to and greater than the respective battery voltage is reached. In the electrical analog this current is analogous to velocity. Thus when the voltage exceeds the coulomb friction voltage E_f, the system can move but the friction voltage is inserted in such a manner as to oppose the motion.

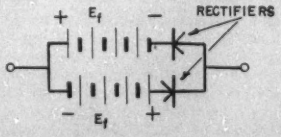

Figure 1. Electric analog for coulomb friction

Batteries with internal impedance sufficiently low to be negligible are easily obtained. It is, however, necessary to consider the forward resistance of the rectifiers and the ratio of forward to back impedance. Dry-type rectifiers or "Germanium" crystals with forward resistances of about 50 ohms and back impedances of 200,000 ohms were used for part of the solutions presented here. For other solutions vacuum tube diodes were used because it was desired to have as part of the analogy a relatively small capacitor in series with the coulomb friction circuit, thereby requiring a much higher back impedance in the rectifiers. Thus, during the period when the system is stopped by the friction force, the discharge rate of the capacitor is truly negligible. For all cases circuit parameters were chosen such that the forward resistance of the rectifiers was only part of the total resistance representing the viscous damping in the physical system.

Basic Systems Studied

The three basic types of system studied are illustrated in Figures 2, 3, and 4, together with their characteristic equations and their electric analogies.

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G. D. McCANN is professor of electrical engineering; F. C. LINDVALL, chairman, division of engineering and C. H. WILTS, assistant professor of applied mechanics with the California Institute of Technology, Pasadena, Calif.

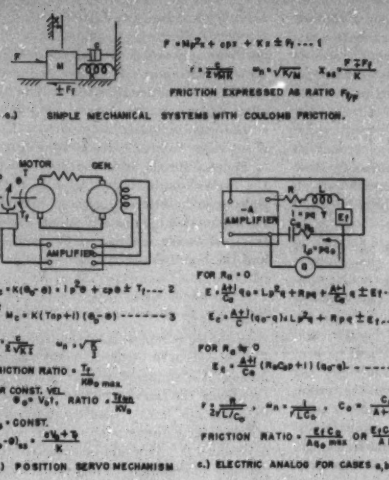


Figure 2. Basic examples of coulomb friction forces in simple systems expressed by quadratic equations $p \equiv d/(dt)$

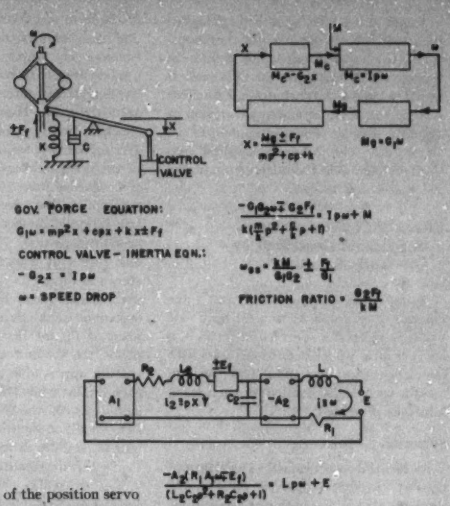


Figure 3. Basic speed governor with coulomb friction in the control system

Simple Systems Represented by Quadratic Equation

Jacobsen[2] and Den Hartog[3] have made a detailed analysis of coulomb friction in a single degree of freedom system, as illustrated in Figure 2*a*, under the action of a steady sinusoidal force. As a starting point, therefore, the results of these earlier analyses were checked with the computer. Also, the action of such a system under suddenly applied constant forces was studied in detail with the computer. Figure 2*b* represents a simple position follow-up type of servo specified by the same form of quadratic equation when only simple error control is employed and all time delays are neglected. Appreciable coulomb friction can be present in the portion of such a system containing follow-up motion θ. The analogous circuit of Figure 2*c* is, therefore, suitable for both physical systems being considered here. Such an analogy has the advantage that low viscous damping can be simulated even though relatively high linear resistance is present in the coulomb friction analog.

In the analogy of Figure 2*c*, the application of a sudden force on the system of Figure 2*a* was simulated by insertion of a voltage in series with E_f. Sinusoidal forces were simulated by sinusoidal current forced into the circuit by the generator G. This same forcing function also simulated the action of the position servo of Figure 2*b* when trying to follow a sinusoidal motion. The follow-up type of servo was studied under the action of suddenly applied constant velocity motion ($\theta_a = v_a t$) by introducing into the analogous circuit the constant current i_a. This class of servo was also studied with two time delays and anticipation feedback.[4]

Solutions in the analog of Figure 2*c* were recorded as follows. For suddenly applied forces in the system of Figure 2*a*, the voltage across the capacitor C_a was recorded since it is proportional to the charge q, which, in turn, is analogous to displacement x. For all other cases in which the current i_a was introduced into the circuit, the voltage across C_a gives the error motion $q_a - q$, which is analogous to $\theta_a - \theta$. To measure the total motion θ of the system, a very large capacitor was inserted in the RL series circuit.

Simple Speed Governor Represented by a Cubic Equation

This basic system is shown in Figure 3 with the electrical analogy that was set up on the computer. This type of governor without coulomb friction is treated in reference 5. As an illustration, a simple flyball governor directly operating a control valve through linear motion is shown in Figure 3, although the characteristic equation studied is of more general application. Here coulomb friction is assumed to be present only in the control system, as it would not be appreciable in the actual machine whose speed is to be controlled. The analysis was made for fixed load torque increments suddenly applied. This condition is simulated in the analogous circuit of Figure 3*b* by the suddenly applied constant voltage E, as shown. The transient speed drop ω is recorded by measuring the voltage across the resistor R_1. The motion x of the control system is proportional to the voltage across the capacitor C_2.

Auto Pilots

The basic system selected for studying auto pilots is shown in Figure 4. Motion in only one axis of the air frame was assumed and interactions between other degrees of freedom were neglected. The transfer equation of the air frame was represented by inertia I and aerodynamic damping c. Any air frame restoring moment was neglected. The control system was assumed to have inertia and a hinge moment spring constant together with both viscous damping and coulomb friction. This system was first studied with feed-back control from the air frame position angle alone. Such servos usually require feedback from the control surface motion and these studies showed this factor to be even more important with large amounts of static friction present. Therefore, the system was also studied with feedback from rudder angle, as illustrated

in Figure 4*a*. In the electrical analogy of Figure 4*b* the voltage across C_a is proportional to the air frame angle θ and the voltage across the capacitor C_1 is proportional to the control surface angle δ. This system was studied under the action of constant torques suddenly applied to the air frame. The suddenly applied constant voltage E in the analogy simulated the disturbing torque.

Effect of Coulomb Friction in Systems Described by Quadratic Equation

As indicated in Figures 2, 3, and 4, the amount of coulomb friction for each case to be considered is specified in terms of a friction ratio whose definition varies with the respective systems. This friction ratio is specifically defined for each set of solutions presented.

Sinusoidal Motion

As mentioned previously, Den Hartog[3] presents a general analysis of a simple mechanical system with one degree of freedom under the influence of a sinusoidal applied force. His analysis is presented in the form of curves showing amplitude and phase angle of the system motion as a function of viscous damping, ratio of applied force frequency to system natural frequency, and ratio of friction force to applied force. These curves were spot checked with good agreement. Den Hartog found a region where the motion of the system departs considerably from that of a sine wave. This occurs for high ratios of friction force to applied force and low ratios of applied force frequency to system frequency. A typical solution in this region as recorded in this paper is shown in Figure 5. This particular solution is illustrative both of the physical system of Figure 2*a* and the servomechanism of Figure 2*b*. With high friction ratios, the system can grab and be held still for appreciable periods of time. At each instant when the force in the system again exceeds the friction force, a transient condition is initiated causing the system to oscillate at its natural frequency. Under certain conditions these free oscillations can reduce the total system force sufficiently to cause successive grabbing or chattering at the high frequency rate. In the situation of Figure 5, the friction ratio is not sufficiently high for this high frequency chattering, and the system is grabbing only on the peak of each swing, the oscillation corresponding in frequency to that of the motion to be followed, or the applied force. As the system breaks away a transient oscillation is initiated. This effect is seen more clearly in the solution of Figure 5 for the error angle because of the higher amplification factor used for that record. With a substantial increase in viscous damping in the system, these transient oscillations can be damped out and the high frequency chattering eliminated.

Response to Suddenly Applied Forces

In Figure 6 typical solutions are given for the system of Figure 2*a* under the influence of a suddenly applied force. In this figure the fraction of critical viscous damping r is 0.01. Solutions were also obtained for values of r equal to 0.1, 0.25, 0.50, and 1.0. As would be expected, the presence of coulomb friction in the same part of a system to which a disturbance is applied, increases the system damping.

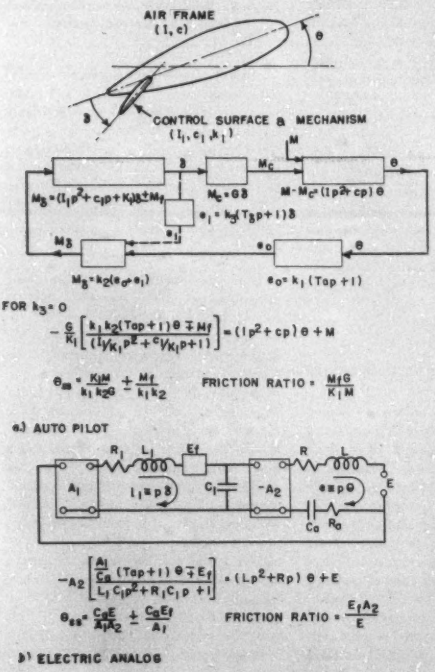


Figure 4. (left). Auto pilot with coulomb friction in the control surface mechanism

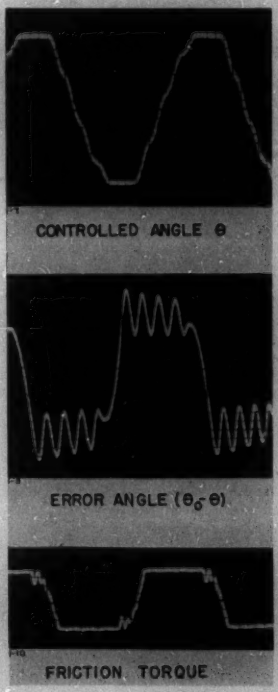


Figure 5. Typical solution to simple angle-position servomechanism following sinusoidal motion when friction ratio $T_f/(K\theta_{max})$ is high and ratio of forcing function frequency to natural frequency is low. $\omega/(\omega_n) = 0.1$, $r = c/(2\sqrt{KI}) = 0.01$

This observation might lead to the erroneous conclusion that coulomb friction in any part of a servomechanism increases system damping. For the single degree of freedom case, coulomb friction without viscous damping causes the system oscillations to decay in the form of an arithmetic progression[8] without change in oscillation frequency. This effect can be seen in Figure 6. As the friction ratio is increased, the system oscillations are snubbed more and more quickly until for a certain value of friction ratio there is no overshoot. For $r=0.01$ this condition occurs at a friction ratio of about 0.50 (see Figure 6); for larger values of r this occurs at lower and lower values of friction ratio. (For $r=0.25$ at friction ratio $=0.30$ and for $r=0.5$ at friction ratio $=0.12$.) However, up to this point the steady-state value of the solution is not appreciably affected by the presence of coulomb friction. For higher friction ratios the steady-state error is $F-F_f/(K)$ since no overshoot is permitted.

The Effect of Coulomb Friction in Speed Governor

The solutions of Figure 8 provide a composite picture of the various effects of coulomb friction as the basic parameters of the speed governor described in Figure 3 are changed. This servo may be described by three basic dimensionless parameters. The first of these, $mG_1G_2/(kIc)$, describes the amount of damping in the linear system without coulomb friction. When equal to one, the system has zero damping. This factor is varied from 0.2 to 2.0 in the solutions of Figure 8. The second parameter, $c/(2\sqrt{mk})$, specifies the amount of viscous damping in the governor alone. Its effect was studied over a range of 0.05 to 3.0 of critical. The third factor, $\frac{G_1}{c}\sqrt{\frac{m}{I}}$ provides the additional parameter required to specify the system, exclusive of the actual time-base or system frequency.

Examination of Figure 8 shows two definite regions in which coulomb friction has diametrically opposite effects. In regions where the per cent of critical damping of the governor system alone is low or the factor $mG_1G_2/(kIc)$ is high so that the system without coulomb friction has little damping, the addition of coulomb friction has a beneficial effect by increasing the damping. This beneficial effect is greatest for low values of viscous damping in the governor system. With $c/(2\sqrt{mk})$ equal to 0.1 or less, very large amounts of coulomb friction can be tolerated and the system thereby damped.

However, an increase in the friction ratio above certain limits results in a decrease in the system response rate and an increase in the steady-state speed drop. It therefore appears practical to rely on coulomb friction under certain conditions for system damping if a maximum value of the disturbance torque can be fixed so that the friction ratio will not drop below the critical value necessary for adequate damping. This critical value of the friction factor was found to vary directly with the third dimensionless parameter, $\frac{G_1}{c}\sqrt{\frac{m}{I}}$.

As an example of this effect, it was found that for $c/(2\sqrt{mk})=0.1$, very satisfactory system performance could be obtained with $mG_1G_2/(kIc)$ as high as 2.0 for a friction ratio as low as 0.07 when $\frac{G_1}{c}\sqrt{\frac{m}{I}}=0.25$. As this last factor is increased the minimum permissible friction ratio must increase proportionally. With less viscous damping, $c/(2\sqrt{mk})=0.05$, good performance can be obtained with $mG_1G_2/(kIc)$ as high as 3.0, with a corresponding lower limit of 0.08 for the friction ratio if $\frac{G_1}{c}\sqrt{\frac{m}{I}}=0.40$. Reduction of this last factor allows a decrease in the friction ratio. As the viscous damping in the governor system is increased, the region in which coulomb friction has a stabilizing effect narrows rapidly and the relative amount of damping it can produce in this stabilizing range decreases.

In the region of sufficiently high viscous damping in the governor system to provide a well-damped solution, the solutions of Figure 8 show that coulomb friction is

Figure 6. Solutions to system of Figure 2a under action of suddenly applied constant force. $r=c/(2\sqrt{KI})=0.01$. Friction ratio $=F_f/(F)$

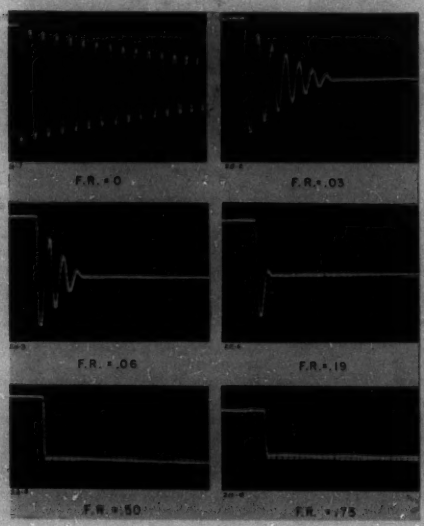
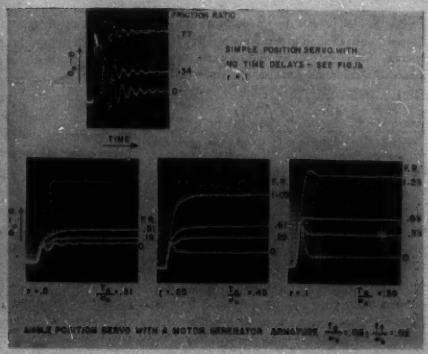


Figure 7. Solutions to basic position follow-up servomechanism of Figure 2b for suddenly applied constant velocity (V_o). Friction ratio $=T_{f_{MR}}/(KV_o)$, $(\theta_o-\theta)_{ss}=cV_o+T_f/(K)$



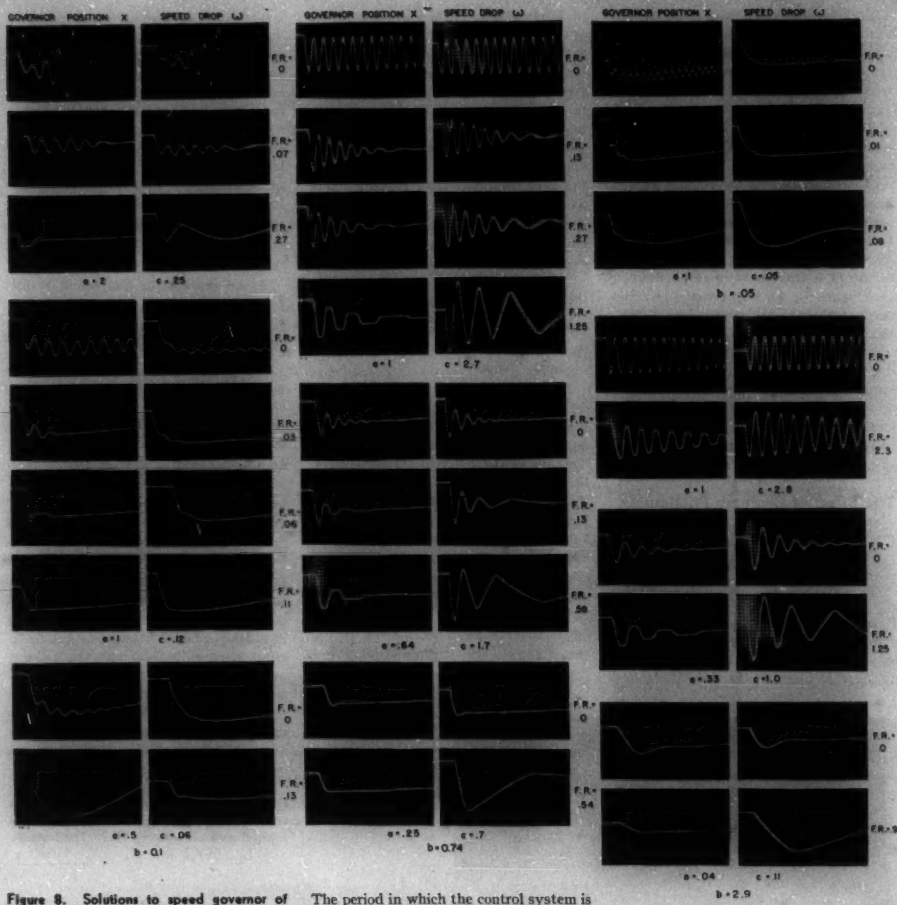
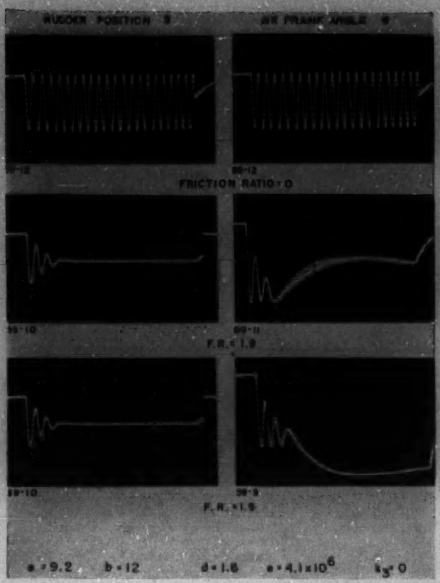


Figure 8. Solutions to speed governor of Figure 3 under action of suddenly applied load torque. Characteristic equation is expressed in dimensionless form by the parameters: $mG_1G_2/(kIc)$, $c/(2\sqrt{mk})$, $G_1/c\sqrt{m/I}$. Friction ratio $= G_3F_1/(kM)$

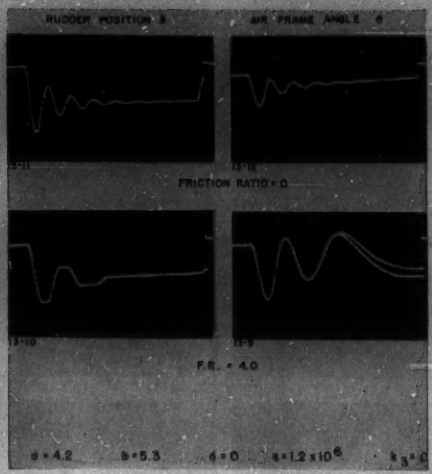
definitely harmful in that it decreases the damping of the speed drop curve and as this friction becomes larger, it causes increased amplitude of oscillation. Where appreciable coulomb friction is present, the frequency of oscillation of both the governor and the controlled system decreases with time. The governor moves in a jerky motion as it is stopped by the coulomb friction at successive intervals. The period in which the control system is at rest increases progressively as the magnitude of motion decreases and the speed fluctuates at correspondingly slower rate. It would appear that under these conditions the system would probably never reach a truly stable condition, but would hunt indefinitely with very low or negligible magnitude. (The magnitude of this hunting would under most conditions probably be unimportant.)

Thus, it is apparent that coulomb friction is detrimental in systems designed for good performance on the assumption of a linear behavior alone. The linear solutions obtained for this system show that without coulomb friction, good system performance can be obtained with the factor $c/(2\sqrt{mk})$ ranging from about 0.3 to 0.8 and $mG_1G_2/(kIc)$ lying between about 0.25 and 0.65. The use of greater viscous damping results in decreased speed of response. As the viscous damping is lowered below this range the required low values of $mG_1G_2/(kIc)$ prevents close regulation.

The general effect of coulomb friction as previously described on speed governors had been found experimentally[7] and has led to the conclusion by some that simple speed governors should be designed with low viscous damping and appreciable coulomb damping. This, however, has the disadvantage that performance is defi-



A—Effect of coulomb friction when system is otherwise undamped



B—Case where coulomb friction stabilizes control surface but destabilizes air frame

Figure 9. Typical solutions to auto pilot of Figure 4. Characteristic equation expressed in dimensionless form by the parameters: $a = T_a/(T_e)$, $b = c_1/(K_1T_e)$, $d = I_1/(K_1T_e^2)$, $e = Gk_1k_2/(K_1cT_e)$ and k_3 where $T_e = 1/(c)$. Friction ratio $= M_1G/(K_1M)$

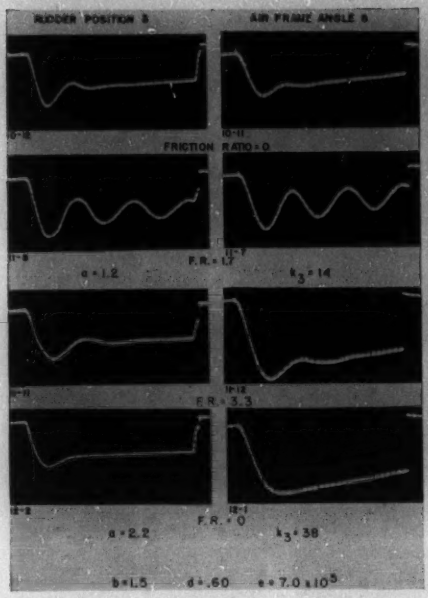
nitely dependent upon the magnitude of the disturbance torque on the system. Better performance can be obtained through the elimination of coulomb friction in favor of stabilization either by viscous damping alone, or by feedback around the governor system itself. With either type of stabilization coulomb friction is unnecessary and generally harmful.

Effect of Coulomb Friction in Auto Pilot

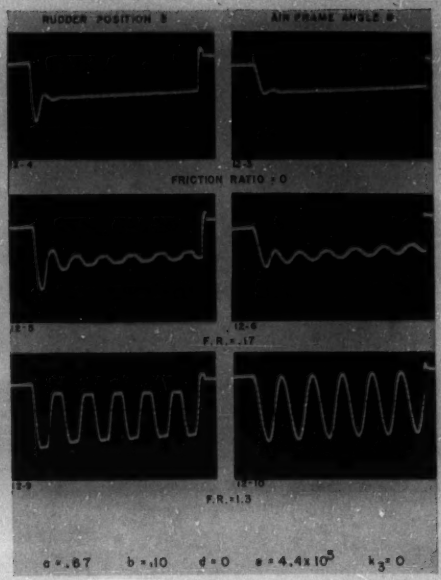
For a general analysis of the auto pilot system described in Figure 4, use was made of the dimensionless parameters listed in the caption of Figure 9. A linear analysis of most systems of this type will indicate the need for feedback from the control surface position. However, to reduce the system to a basic minimum, this feedback was omitted from the first studies. Under these conditions the general conclusions described for the speed governor were also found to be valid for the auto pilot. The region in which coulomb friction increased damping was much narrower for the auto pilot and no performance that could be considered satisfactory for such systems was obtainable with appreciable coulomb friction. Figure 9*a* is a typical solution of a system undamped without the presence of coulomb friction, but positively damped by the addition of coulomb friction. This set of records is of interest in that the indeterminate and critical nature of the steady-state value of the air frame error angle is shown. As indicated by the equation for the steady-state error angle, given in Figure 4, this value may lie anywhere within the two indicated limits.

The two solutions of Figure 9*a* were obtained by changing very slightly the gain around the complete feed-back loop. In most cases it was found that in a system showing good performance without coulomb friction, the addition of coulomb friction destabilized both the control surface and the airframe. However, there was found to be a limited range in which the addition of coulomb friction actually stabilizes the rudder position, although it causes destabilization of the air frame. This behavior is illustrated in Figure 9*b*, and the limited range was found by varying only the dimensionless parameter $Gk_1k_2/(A_1T_e)$, the performance is still unsatisfactory.

The top four solutions of Figure 9*c* represent a system using feedback from the control surface that provides good performance in the absence of coulomb friction. With coulomb friction added, both the control surface and the air frame are destabilized. As indicated by the bottom set of four solutions, the oscillations produced by coulomb friction could be damped out by readjusting both the parameter *a* and the feed-back constant k_3. In general the destabilizing action of coulomb friction could be eliminated by raising both the anticipation time constant T_a and the gain around the control surface position feed-back loop. For friction ratios as high as 1.7, doubling both the gain around the feed-back loop and the time constant T_a appears to be sufficient in most cases. However, the time of response of the system decreases and steady-state error increases, as shown in Figure 9*c*. Figure 9*d* illustrates a case involving no coulomb friction in which good system performance is obtainable without gain around the rudder position feed-back loop and for which the addition of such a gain has little beneficial effect. This was found to be a general effect when the relative inertia of the control



C—Solution showing effect of increased feed back (k_3) from control surface position to counteract coulomb friction effect



D—Case where coulomb friction destabilizes both control surface and air frame

surface system is low and good damping without rudder position feedback is obtainable.

In the study that was made of the effect of coulomb friction in three basic types of servomechanisms, it was found that the presence of such friction in the follow-up mechanism of a positioning servo always increases the system damping, but also increases the time of response and the steady-state error. The least value of friction ratio that is appreciable in such systems varies with the relative amount of viscous damping, increasing about proportionately with the per cent of critical viscous damping. Its effect is shown in detail in Figure 5. The chattering or grabbing effect described by Den Hartog for a single degree of freedom mechanical system under the action of a sinusoidal applied force was also found to be present in all servomechanisms under the action of both steady-state sinusoidal disturbances and transient disturbances.

In simple speed governors described by a cubic equation, coulomb friction can either increase or decrease the system hunting, depending upon the magnitude of the basic linear parameters of the system. Friction decreases the hunting when the viscous damping in the governor system itself is low, and as long as the disturbance torque does not exceed certain limits, its performance can be considered to be beneficial. In regions where good performance is obtained through sufficient viscous damping or feedback around the governor loop, the presence of coulomb friction can, in general, be considered as harmful. The magnitude of coulomb friction is defined by the friction ratio and the value that is appreciable can best be determined from examination of the typical solutions presented in the paper.

Coulomb friction was found to be generally harmful in the basic auto pilot studied here. The friction would under certain conditions increase the system damping slightly, but in general would decrease damping in both the control surface and the air frame. The destabilizing effect of friction can usually be corrected somewhat by increasing the anticipation time constant T_d and, although only a limited number of cases was studied it is indicated that if the addition of feedback around the control surface loop is beneficial in the linear analysis of the system, good damping, even with the presence of large amounts of coulomb friction, can be achieved by doubling both the anticipation time constant and the gain around this feed-back loop.

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No Discussion

Graphical Analysis of Control Systems

WALTER R. EVANS
ASSOCIATE AIEE

Synopsis: The purpose of this paper is to demonstrate some graphical methods for finding the transient response of a control system. A simple position follow-up system is considered for convenience although the method is applicable in the same form for higher order systems or those in which only empirical frequency data is known. The basic procedure is to find the roots of the differential equation which correspond to the exponential transient terms which dominate the response. Doctor Profos[1] of Switzerland points out that the plot of the function which describes the system from error to output is a function of a complex variable of which frequency is the imaginary part and damping is the real part. The Nyquist plot is thus one line of a conformal map with the root of the equation being the value of the variable which makes the function equal to -1. Any line of plot can be calculated for systems with known functions with essentially the same ease as the Nyquist plot by use of some graphical tricks. The amplitude of any transient term is determined from the plot once the root is known by use of a theorem of operational calculus. The development possibilities of the subject seem to be very great as suggested by several topics not yet investigated.

Review of Fundamentals

A QUADRATIC SYSTEM will first be analyzed in order to emphasize the important concepts in finding any transient response. Consider the position follow-up system shown in Figure 1.

The differential equation relating the output to the error is

$$K\epsilon = \left(1+T_M\frac{d}{dt}\right)\frac{d}{dt}\theta_o(t) \qquad (1)$$

K is the output speed corresponding to a unit error. T_M is the time constant of motor acceleration, other delays are neglected. But input is the output plus the error.

$$\theta_i(t) = \theta_o(t) + \epsilon(t) = \left[1+\frac{1}{K}\left(1+T_M\frac{d}{dt}\right)\frac{d}{dt}\right]\theta_o(t) \qquad (2)$$

Consider the input to be a unit step and note that the steady state value of output will also be unity. Assume that the output transient can be represented by exponential terms, so that $\theta_o(t) = Ae^{st}$ is substituted into the differential equation, and the common factor Ae^{st} cancelled.

$$0 = 1+\frac{1}{K}(1+T_M s)s \qquad (3)$$

Note that s appears at each point where d/dt had occurred before. This equation in s is an algebraic one, and any value of s which satisfies it represents an exponential term which can exist in the transient.

Anticipating the fact that s will replace d/dt when an exponential solution is assumed, the system itself can be more conveniently represented by the block diagram of Figure 2. The function in a block represents the ratio of its output to its input. The relationship between θ_i and θ_o can now be set up directly.

$$\frac{\theta_i}{\theta_o} = \frac{\theta_o+\epsilon}{\theta_o} = 1+\frac{\epsilon}{\theta_o} = 1+\frac{1}{K}(1+T_M s)s \qquad (4)$$

For this case of a quadratic equation the roots can be found directly by completing the square and solving for s.

$$s = -\frac{1}{2T_M} \pm j\sqrt{\frac{K}{T_M}-\left(\frac{1}{2T_M}\right)^2} = -\sigma_n \pm j\omega_n \qquad (5)$$

The oscillatory case is taken because it is typical of the response of a fast control system. The transient solution is the sum of two exponentials, one for each root.

$$A_1e^{(-\sigma_n+j\omega_n)t}+A_2e^{(-\sigma_n-j\omega_n)t} \qquad (6)$$

But this can be converted to a cosine function using the relation

$$e^{j\omega t} = \cos\omega t + j\sin\omega t \qquad (7)$$

Select as the new undetermined constants the amplitude A and the phase angle ϕ

$$Ae^{-\sigma_n t}\cos(\omega_n t-\phi) \qquad (8)$$

The constants are determined from the initial conditions that the output is zero and its rate of change is zero at time zero. The complete solution for output then becomes:

$$\theta_o(t) = 1-\left(\frac{1}{\cos\phi}\right)e^{-\sigma_n t}\cos(\omega_n t-\phi); \quad \text{in which } \tan\phi = \frac{\sigma_n}{\omega_n} \qquad (9)$$

The following numerical values are selected for convenience

$$K = 2/\text{seconds} \quad T_M = 1 \text{ second} \qquad (10)$$

Note that if T_M were equal to one-tenth second, the value of θ_i/θ_o given in equation 4 would be the same if s and K were both made ten times larger. Thus, the results of these problems can be shifted into any range of values with which the reader may be normally accustomed. Substituting the foregoing values gives

$$\theta_o(t) = 1-1.07e^{-0.5t}\cos(1.32t-21°) \qquad (11)$$

Graphical Plot to Locate Real Roots

The consideration of an additional delay in the control system raises the degree of the equation from second to third. In this case, consider the delay to be the time constant of the inductive build-up of current in the field of the generator supplying the motor. Setting up the ratio θ_i/θ_o

$$\frac{\theta_i}{\theta_o} = 1+\frac{\epsilon}{\theta_o} = 1+\frac{1}{K}(1+T_G s)(1+T_M s)s \qquad (12)$$

The previous values of $K=2, T_M=1$ will be kept and T_G taken as one-fourth second.

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WALTER R. EVANS, assistant professor, department of electrical engineering, Washington University, St. Louis, Mo.

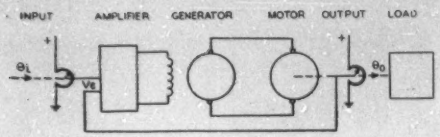


Figure 1. Position follow-up system

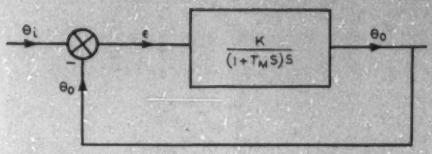


Figure 2. Simplified block diagram

Complex roots such as those which arose in the quadratic system always occur as a conjugate pair; therefore this third degree equation must have either one or three real roots. In finding the real value of s which makes the function of s of equation 12 equal to zero, one of the reasons for the simplicity of finding roots of a known function will become apparent. The factors of ϵ/θ_o are already known therefore, make $\theta_i/\theta_o = 0$ by making $\epsilon/\theta_o = -1$. In guessing s to be a negative real quantity $-\sigma$, each factor of ϵ/θ_o is a real quantity and can be plotted against $-\sigma$ as shown in Figure 3.

The range of $-\sigma$ from 0 to -1 gives a product of factors which is negative, but the magnitude is a fraction so that a root cannot exist in this region. The region beyond $-\sigma = -4$ certainly contains a root. A guess of $-\sigma = -5$ gives a result of $\epsilon/\theta_o = -2^1/_2$. A second guess of $-\sigma = -4^1/_2$ suggests itself to make $\epsilon/\theta_o = -1$ because the factor $1+T_G S$ is then cut in half whereas the other factors are nearly constant. The new result is $\epsilon/\theta_o = -63/64$ with further correction probably not justified by accuracy of the data. Note the calculation of ϵ/θ_o is very rapid by maintaining the identity of the factors, and that guesses of values of roots are immediately suggested.

Divide by the factor $s+4^1/_2$ to reduce the third degree polynomial to a quadratic. Solve for the complex roots of the quadratic as before to find $s = -0.25 \pm j1.30$. Apply initial conditions to determine the constants to find that the total response is given by

$$\theta_o(t) = 1 - 0.08e^{-4.5t} - 1.036e^{-0.25t} \times \cos(1.30t - 27°) \quad (13)$$

The main effect of considering the second delay is to cut down the damping rate of the oscillation.

Vector Plot of Error–Output Ratio For Sinusoidal Signals

A standard method of counteracting the effect of a time delay is to insert in the amplifier a circuit whose output includes the derivative of error. The form of its output/input function is shown in the block diagram of Figure 4.

The determination of the roots of such a system is generally agreed to be a tedious job by present methods and is infrequently done. The frequency response method instead has been developed to a fine point as indicated by the many recent articles on the subject.[2] In this method the feed-back loop is broken and a sinusoidal signal is impressed. The output is determined in magnitude and phase angle as a function of frequency. This information can be obtained by any one of several calculating methods or by direct laboratory tests. A convenient way to show the results is to plot the vector ratio of error/output. The plot for the foregoing system is shown in Figure 5 with the frequency identified by numbers in parenthesis.

For a stable system, the locus of ϵ/θ_o must swing outside the -1 point, physically meaning that the output/error ratio is a fraction at the frequency such that the output is in phase with the error. The vector ratio of θ_i/θ_o is greater than the ϵ/θ_o ratio by 1 and is therefore a vector to the curve with tail at the -1 point. Increasing the gain to $K=6$ contracts the curve as shown bringing it inside the -1 point indicating an unstable system. An intermediate value of gain $K=3$ contracts the curve in toward -1 point so that one would expect that the system was closer to becoming unstable, or having less damping. A quantitative value of damping, however, is now primarily a matter of experience based on systems with known vector plots and known transient performance. It is precisely at this point that the key idea of Profos becomes effective.

Determination of Principle Roots From Vector Plot

The key idea of P. Profos[1] is to consider the vector plot to be the base line from which the complex roots of the main damped sinusoidal term can be determined. Recall from the simple quadratic system that these roots make the ϵ/θ_o ratio equal -1. These roots could be determined if a plot of the function of s could be made as a function of both $-\sigma$ and ω.

One recognizes a somewhat similar situation in plotting an electrostatic field in which the complex variable involves flux and voltage. The patterns for lines of constant voltage and lines of

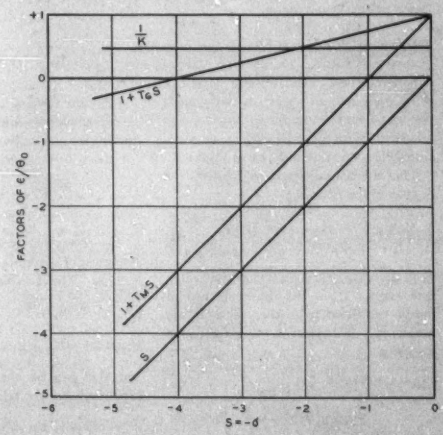


Figure 3. Plot to locate real roots

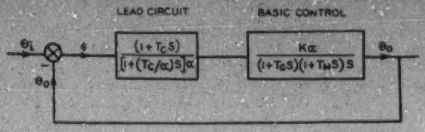


Figure 4. Block diagram of system. Lead circuit added to amplifier. Generator and motor delays considered

flux is known to be a grid of curvilinear squares. If this flux plotting property can be justified for ϵ/θ_o, the root values of $-\sigma_n$ and ω_n can be determined by sketching. The justification is that ϵ/θ_o is a function of s which has a particular derivative with respect to s for any value of s in the region of interest. Thus in Figure 6 the change of ϵ/θ_o along the curve for an increment $\Delta j\omega$ means that the derivative must be located 90 degrees clockwise from this increase. If the change in variable at this point were $-\Delta\sigma$ instead, the change in the ϵ/θ_o function would be opposite in direction to the derivative. The magnitude of the change of the function would be the same for equal small changes $-\Delta\sigma$ or $\Delta j\omega$. Thus for fairly large, but equal changes in $-\sigma$ and ω, a set of curvilinear squares will be formed as indicated by the dotted lines.

Note that this complex plot can be based on an original vector plot obtained from laboratory data as well as from one which is calculated. This is valid however, only for systems which are linear in the test range and the results are applicable only to this linear range. This restriction is necessary because the justification of the curvilinear square pattern is that the derivative of ϵ/θ_o is dependent only on the nature of the signal, not the amplitude.

One can sketch such a grid with more confidence after having calculated a few systems with known functional form. This can be done conveniently by some graphical tricks, as shown in the next section.

Calculation of Complex Plot

First consider the facts about present calculation methods as a function of frequency alone which simplify that task. The ratio of output to input for any one time delay is a vector quantity specified by amplitude and phase angle. The output to input ratio for several time delays in series is then the product of their amplitudes and a sum of their phase angles.

$$(1+j\omega T_1)(1+j\omega T_2) = A_1 e^{j\phi_1} A_2 e^{j\phi_2} = A_1 A_2 e^{j(\phi_1+\phi_2)} \quad (14)$$

If one keeps track of amplitudes logarithmecally, the product can be taken by adding logarithms.

$$\log A_1 A_2 = \log A_1 + \log A_2 \quad (15)$$

The logarithmic scale commonly used is the decibel scale defined by the equation below.

$$\text{decibel} = 20 \log_{10} A \quad (16)$$

The new problem introduced is to express any one term $1+T_s$ as an amplitude and phase angle. Several schemes are of course possible, but the following is believed to be the most convenient. Consider the term to be factored as shown below with the T factors saved for later consideration.

$$1+Ts = T\left(\frac{1}{T}+s\right) \quad (17)$$

The term s can be located as a vector from the origin. The complete vector is one with tail at the $-1/T$ point and head at the s point. This vector can be measured by a protractor pivoted at the $-1/T$ point with scale in line with the s point. The amplitude is desired in decibels so the scale is so marked. This scale can be checked by noting the magnitude of 1 is marked as 0 decibel and the magnitude of 2 as 6 decibels in Figure 7.

The case of a quadratic term in the ϵ/θ_o ratio frequently arises. Such a term can be broken into two factors having conjugate complex roots as shown below.

$$S^2+as+b = [S-(-\alpha+j\beta)][S-(-\alpha-j\beta)] \quad (18)$$

These conjugate roots are thus the pivot points for the protractor in making measurements to the s point.

The procedure is now simply one of tabulating decibels and angles for each term, adding decibels for the total decibels, and adding angles for the total angle of the ϵ/θ_o ratio. The over-all amplitude factor for the product can now be established by checking the special case of s is equal to 0. The value of ϵ/θ_o for $s=o$, eliminating K and s factors is 1 as shown in the block diagram. The sum of the decibels reading with protractor swung to the origin actually obtained is due to all of the factors accumulated in setting up the $1/T+s$ vectors. This decibel value should be subtracted from all sums obtained for other values of s. Division by the gain K is achieved by subtracting the decibels for that K from the net decibels previously obtained. The ϵ/θ_o vector is then known as an angle and decibels of magnitude and so can be plotted using the same protractor.

It is convenient to make the first calculation for constant values of $-\sigma$ and the uniform changes in $j\omega$. Thus a curvilinear square pattern should be formed with any mistake in calculating one point showing up by its failure to be in line with the grid set up by the other points. The plot for the system of Figure 4 is shown in Figure 6 for $T_C=1/2$ and $K=4$. The

Figure 5. Vector plot of error/output ratio with sinusoidal signal impressed

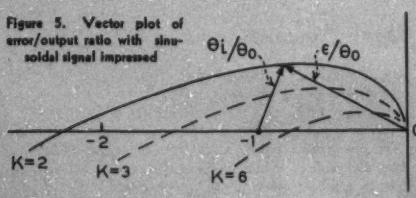
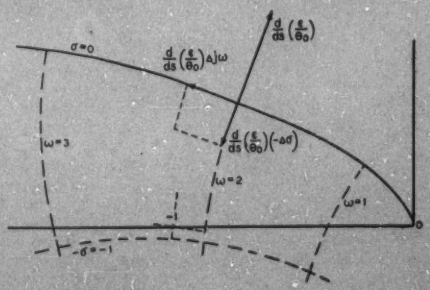


Figure 6. Complex plot for system. Conformal map properties are indicated. Root at $s=-\sigma_n \pm j\omega_n = -0.95+j2.25$



value of s which makes $\epsilon/\theta_o = -1$ is $s = -\sigma_n \pm j\omega_n = -0.95 \pm j2.25$. The amplitude of the transient having this root as well as its initial phase angle, needs to be determined before the transient can be plotted. Fortunately this is possible without need for finding the rest of the roots and substituting initial conditions by means of a theorem of operational calculus.

Amplitudes From Operational Calculus

The concept of s thus far presented has been simply that of being a complex number used in the exponential solution of the differential equation. This concept is sufficient for explaining the process of finding the roots to the equation. The symbol s, however, has a more potent significance as it is used in operational methods such as the Laplace Transform.[3] Those familiar with these methods of course realize that this is a long study in itself, but essentially only one fact need be used. The amplitude of any transient term is given in terms of its root by

$$A_1 = \frac{1}{s\left.\frac{d}{ds}\left(\frac{\theta_i}{\theta_o}\right)\right]_{s=s_1}} \tag{19}$$

But the derivative of the function can be determined directly from the complex plot. It is vector with an angle of 84 degrees as shown in Figure 6. The magnitude is the change in ϵ/θ_o divided by the change in s. An average of several measurements in the region of the -1 point gives the value of 0.60.

The two transient terms involving the pair of complex roots can be converted into a single term as in the simple quadratic system. The conversion is made very rapid by noting that the amplitude terms are conjugates of each other. In taking the sum therefore their real parts add and their imaginary parts cancel. The result can thus be written as twice the real part of one of them.

$$2R\left[\frac{e^{-\sigma_n t}e^{j\omega_n t}}{ae^{j\alpha}be^{j\beta}}\right] = \frac{2e^{-\sigma_n t}\cos\ (\omega_n t - \alpha - \beta)}{ab} \tag{20}$$

in which

$$ae^{j\alpha} = s = -\sigma_n + j\omega_n$$

and

$$be^{j\beta} = \left.\frac{d}{ds}\left(\frac{\theta_i}{\theta_o}\right)\right]_{s=-\sigma_n+j\omega_n}$$

The other two roots to this fourth degree system are found to be real roots of -2.58 and -16.6. The derivatives can be found by any one of several methods but from the slope of the function they are -1.45 and $+1.66$, respectively. The complete solution including the steady state value of 1 becomes:

$$\theta_o(t) = 1 - 1.36e^{-0.95t}\cos\ (2.25t - 17°) + 0.267e^{-2.58t} - 0.036e^{-16.6t} \tag{21}$$

Procedure For Complicated Systems

A solution of a fourth degree equation is nothing new, but this method can readily be applied to higher order equations. Thus additional time delays or stabilization circuits will distort the frequency locus but the conformal map can still be sketched.

The quadratic roots found from the original plot represent the transient term which will frequently dominate the complete transient response. If a complete solution is desired, however, these known factors can be divided out of the θ_i/θ_o ratio. Thus, for any value of frequency, the angle and decibels readings for the protractors pivoted at the conjugate root points, with the scale at the frequency point, should be subtracted from those of original vector to obtain those of the new vector. Note that in general the portion of the original vector plot for higher frequency values will now be contracted into the -1 region. Thus, one would have to have frequency data in the region of the higher order roots in order to find them. Usually the corresponding transient term will be found to damp out rapidly so that they would be needed only to study the initial break away of the output. It is probable that other methods would be more applicable to studying this region.

The construction of a vector plot for a multiple loop system can be readily carried out on a completely vector basis. The multiplication process has already been shown, and the addition process is simply the familiar completion of a parallelogram. Many time saving tricks are possible however by shifting or rotating an entire plot with respect to its previous position. The predominance of feed-back signals over feed-forward signals makes the inverse plot more convenient, for starting at the output one can build up a diagram back to the input step by step.

Occasionally systems will have two pairs of quadratic roots in about the same frequency range. The Nyquist plot then circles the -1 point with the result that a plot built up from one side overlaps a plot built up from the other side. This situation can be handled by a process of successive approximations. Start with the pair of complex roots suggested by the plot built from one side and divide the function by these factors. The resultant plot will usually give good indication of the other pair of roots since overlapping is eliminated. The first pair of roots may now be determined more accurately by dividing the original plot by the factors corresponding to the second pair of roots.

For systems in which the damping is near critical, the behavior of the plot near the origin must be understood. A good example to work out is for a simple quadratic with roots of $-1 \pm j^1$. The first "square" is shaped like a triangle with base from 0 to -1, and curved sides crossing at $-1/2 + j^1$. The missing corner will be found to be at the midpoint of the 0 to -1 line.

Protractor measurements could be eliminated by substituting potentiometers at the pivot point which turn in resistance proportional to angle. The decibel readings could similarly be replaced with potentiometers which are turned as a logarithmetic function of the distance to the s point. Connecting each set of potentiometers in series will then give total angle and total decibels as resistances. These resistances could then be used as inputs to instrument servos which could actuate meters or locate the position of a pen.

Development Possibilities

What are the possibilities for new types of laboratory tests to give empirical data other than just frequency response? The problem would seem to be primarily one of achieving a steady state condition long enough to get a reading and not have the natural transient of the system present. An exponential build-up might work in that starting from zero the transient should not appear and the only reading necessary is the ratio of output to input, which should be constant during the build-up. An exponentially increasing sine wave would correspond to a complex value of s, though the reading here would have to include phase angle as well as magnitude.

The similarities of the conformal mapping properties of static fields and these functions of s seem to offer the most interesting possibilities. Conceivably the known system information could be set up as boundary conditions of an electrostatic field so that the pattern of equipotential lines and lines of flux would correspond to the system plot. The system plots of the type described in this paper

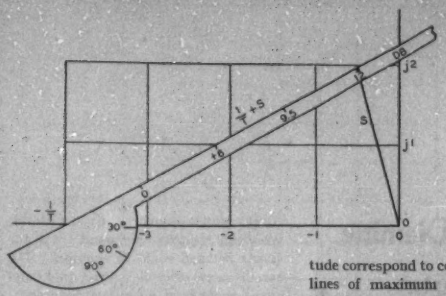


Figure 7. Vector determination of $1/T+s$

however, have several values of s which makes the ϵ/θ_o equal to -1 where as only one voltage could exist at the -1 point for the field. Another system plot is possible however in which the roles of ϵ/θ_o and s are reversed. Any point on the plane would correspond to a value of s and ϵ/θ_o is plotted as loci of its constant real part and constant imaginary part.

A physical picture of the plot can be gained by considering a surveying contour map in which lines of constant altitude correspond to constant real part and lines of maximum slope correspond to lines of constant imaginary part. On this basis a quadratic function in the region of the roots has the shape of a horse's saddle. Imagine a view of the saddle from above with the origin at the horn of the saddle and the $-\sigma$ axis running straight back to the rear. One trace of zero imaginary part is along this axis and the other intersects it at right angles at the center of the saddle. Picture now the saddle to be immersed in water and gradually lifted out. The water level lines on the saddle give the locus for a real part of $\epsilon/\theta_o = -1$ as the gain of the servo is increased. In this situation the roots of the transient equation lie at the intersection of the locus for a real part of $\epsilon/\theta_o = -1$ and the locus for zero imaginary part. These roots therefore move down the saddle from the horn and the rear until they meet in the center for the critical damping case. Further increase in gain, or raising of the saddle, causes the roots to appear as a conjugate pair moving down each side toward the stirrups.

Will a static field be capable of mapping such a pattern? If so, what boundary conditions will be necessary? This seemingly never-ending chain of questions serves to keep the subject interesting, since one does not know what useful method might be encountered in the process of finding the answers.

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No Discussion

Crystal Filters Using Ethylene Diamine Tartrate in Place of Quartz

E. S. WILLIS
MEMBER AIEE

Synopsis: Ethylene diamine tartrate (EDT) crystal filters were developed to replace the earlier quartz type channel filters in the broad band carrier telephone systems, because of the threatened scarcity of quartz. These new filters give performance comparable to that of the earlier design. The growth of the EDT crystals from seeds and their fabrication into crystal units for use in filters are covered in companion papers on "Design and Performance of Ethylene Diamine Tartrate Crystal Units"[6] and "Growing Crystals of Ethylene Diamine Tartrate."[7]

UNUSUAL RESULTS are often realized in the search for improved methods of providing economical telephone communication. The commercial application of ethylene diamine tartrate (EDT) crystal units to electric wave filters for use in carrier telephone systems was one such development. The immediate urge for this development was the postwar need for an enormous increase in long distance telephone channels. Whereas the prewar program called for the addition of about 400 long distance channels annually, it was evident, soon after V-J day, that the postwar program would call for about 10,000 such channels annually.

New long distance channels are provided in the telephone plant by the broad band carrier systems[1,2,3] the designs of which are based on handling a bank of twelve speech channels as a unit. These 12 channels make efficient use of the available frequency space by using only 4 kc for each speech channel. High quality transmission is realized by the use of filters having sharp cut-off characteristics. Hence, the filters used to separate channels, to supply the carrier frequencies, and to supply and pick off the pilot frequencies all have been filters employing quartz crystal units.

During the latter part of 1945 it appeared that the expansion of long distance facilities would be curtailed due to the scarcity of quartz. The anticipated quartz demand was approximately ten times the prewar imports from Brazil and it was estimated that such quartz, suitable for telephone applications, as was left over from the war effort soon would be exhausted and the manufacture of broad-band carrier systems cease unless prompt measures were taken to remedy the situation. This emergency was met ultimately by the use of EDT crystal filters developed principally as the result of the forward looking research program of the Bell System under which piezoelectric materials have been under investigation for more than 25 years.

In order to allow time for the development and manufacture of EDT crystal filters for commercial service the following measures were taken to provide immediate relief to the critical quartz situation:

1. It was found possible to stimulate the rate of quartz purchases from Brazil.

2. More efficient use was made of the available quartz.

During the period of 1938 to 1942 annual imports of 5,000 to 7,000 pounds of quartz, of a size suitable for use in filters, were sufficient to meet the yearly demands of the Bell System. At the beginning of 1946 the outlook was that imports might be stimulated to reach a maximum of 20,000 pounds annually. Even with this optimistic estimate only about 1/4 of the quartz demand could be supplied. The use of more efficient cutting tools and improvements in manufacturing techniques introduced during 1946 resulted in a marked increase in yield of quartz plates per pound of quartz and further reduced the spread between supply and demand. These steps resulted in stretching the availability of quartz and will make it possible to carry out an orderly shift from quartz to EDT without requiring any significant drop in the rate of supplying broad-band carrier systems.

The decision to develop EDT crystal filters as a permanent means for relieving the quartz shortage was made after conducting an extensive survey. This included an analysis of the need for quartz for various filter functions, and a consideration of alternate means for accomplishing these same objectives while either eliminating or greatly reducing the use of quartz. Seventy-five per cent of the quartz required for telephone use is utilized in the manufacture of the channel selecting filters in broad-band carrier systems. There are 12 of these filters operating in the frequency range of 60 to 108-kc and the channels with which they are associated are numbered 1 to 12, inclusive, starting at the 108-kc end of the frequency band. The lower frequency filters of the group, channels 9 to 12 inclusive, require the larger size quartz plates; hence, they account for a large portion of the quartz demand.

Large quantities of quartz also are required for the pilot pick-off filters in the carrier on cable systems. There are three of these filter designs passing narrow bands of frequencies at 12-kc, 28-kc and 56-kc, respectively. They each require large quartz plates which can be obtained

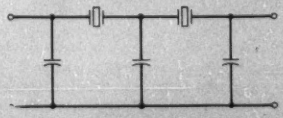

Figure 1. Schematic circuit of the 56-kc pilot pick-off filter (246A)

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E. S. WILLIS is with the Bell Telephone Laboratories, Inc., New York, N. Y.

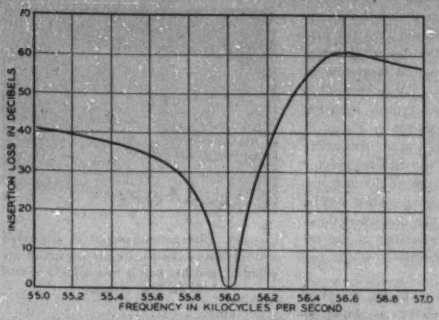

Figure 2. The 246A filter picks off the 56-kc pilot signal without disturbing the modulated speech signals carried on the line

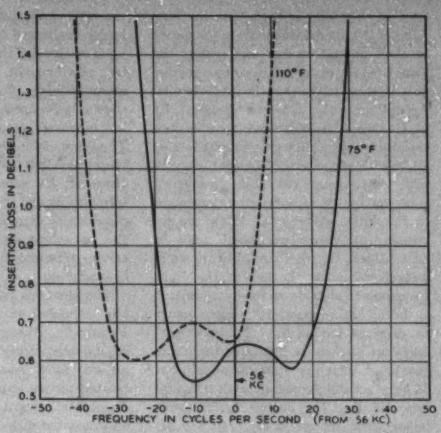

Figure 3. With wide fluctuations in temperature the loss at 56 kc remains practically constant

only from the extremely scarce large size crystals. Of these three the 56-kc filter requires the most quartz; hence, it was placed next to the channel filters on the list of filters to be redesigned or replaced in order to reduce the demand for quartz.

The means for reducing quartz demands centered about possible modifications of the bank of 12 channel filters. Several proposals were considered involving either, the use of quartz type filters operating at higher frequencies where the size of the quartz plates would be greatly reduced, or the use of coil and condenser filters. In either case additional stages of group modulation would have been required, necessitating additional central office equipment. These proposals were discarded as being too costly in manufacturing effort and office space required. The most attractive proposition involved the redesign of the 12 channel filters to use EDT crystal units in place of the quartz units. This not only maintained the same operating frequency range as the present circuits but also provided the following advantages:

1. The EDT filters would be designed to meet practically the same electrical requirements as the quartz filters, thereby requiring only minor systems circuit changes.

2. The EDT filters would occupy the same mounting space as the quartz filters, eliminating the need for extensive equipment design changes.

3. An independent source of piezo-active material would be provided so that growth of the long distance facilities in the future will not be restricted by the inability to import sufficient quartz.

It was felt that the stockpile of quartz would be extended sufficiently to permit the orderly completion of this development without creating a break in the manufacture of broad-band carrier systems.

After the decision to develop EDT crystal units for use in channel filters had been reached it appeared relatively straightforward also to apply EDT crystal units to the 56-kc pilot pick-off filters. The selection of the type of crystal cut for use in a particular filter is based on the use to which the filter is put. The pilot pick-off filter is designed to select a narrow band of frequencies around 56 kc and maintain practically constant loss to these frequencies with temperature variations and aging. This filter is used in central offices and in attended repeater stations, hence the temperature range is about 70 to 110 degrees Fahrenheit. The *A*-cut EDT crystal unit, which is very stable with temperature changes in the region of room temperature, was selected for use in this filter. The *Y*-cut EDT crystal unit, because of its low equivalent inductance, was selected for use in the lower frequency channel filters that is, channels 7 to 12, inclusive. In order to expedite the introduction of EDT crystal units and filters in manufacture, the filters for channels 1 to 6, inclusive were originally designed to utilize quartz crystal units. The selection of the specific cut of EDT crystal unit for substitution in these filters has not been made.

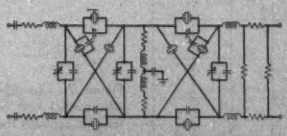

Figure 4. Schematic circuit of the crystal channel filter (239 type)

56-Kc Pilot Pick-Off Filter

The 56-kc pilot pick-off filter (*246A*) consists of two EDT crystal units and three fixed condensers as shown by the schematic of Figure 1. The greater piezoelectric effect of EDT crystals made possible the use of a simpler type of filter structure than was used in the earlier quartz filter (*226C*) which it replaces. The two crystal units required in the filter are identical and they are 2-terminal units rather than the more complicated 4-terminal units used in the quartz design. The insertion loss frequency characteristic shown in Figure 2 is similar to that of the quartz filter. The filter mounts interchangeably with the quartz filter and, as with the earlier design, two filters are connected in tandem in each twist amplifier of the cable carrier system to select the pilot frequency and suppress adjacent bands. Any combination of quartz and/or EDT filters may be used satisfactorily in the system.

The 56-kc pilot frequency is used to regulate the gain of the twist amplifier and any changes in the pass band loss of the filters are reflected as errors in the regulation of the amplifier. Therefore, it is desirable to maintain the loss of the filter at 56 kc as constant as possible with temperature variations The temperature coefficient of the EDT crystal units

used in this filter is such that the pass band of the filter shifts down in frequency as the temperature increases and at the same time the pass band loss increases due to a decrease in the Q of the crystal units. Accordingly, the filter as designed by G. H. Lovell, utilizes these two effects to maintain approximately constant loss at 56 kc with temperature change. At normal temperature the 56-kc frequency rides on the top of an attenuation bump in the filter pass band. With the increases in temperature which may be encountered, the pass band shifts down in frequency causing the loss at the 56-kc frequency to slide down from its location at the top of the bump but the accompanying decrease in Q of the crystal unit produces an increase in loss over the entire pass band, thereby maintaining the loss at 56 kc practically constant with temperature change. The change in loss with temperature change from 75 to 110 degrees Fahrenheit is shown in Figure 3. For two filters in tandem this amounts to only 0.15 decibel at the 56-kc frequency.

Channel Filters

The EDT crystal channel filters (239 type) were designed to replace the earlier quartz type filters[4] (219 type) in broad band carrier systems. They mount interchangeably with the earlier filters. In order to permit the placing of wider tolerance on the inductance and resonant frequency of the crystal units, these filters were designed as two-section rather than one-section lattice type filters. However, instead of using four retardation coils as was done in the earliest crystal channel filter[5] (75 type) only three coils per filter were used. The two center series coils were replaced by one shunt type coil. This not only permitted the assembly of the filter in the same size container as the 219 type and kept the pass band loss level of the two designs the same but also brought about a reduction in manufacturing effort. The schematic is shown in Figure 4. A typical insertion loss characteristic is shown in Figure 5. The edges of the pass band are somewhat more rounded than the earlier designs. However, the same type of channel performance in the telephone system will be obtained by shaping the characteristic of the voice frequency amplifier at the receiving terminal to effectively widen the channel band.

The elements that make up the filter are assembled on a chassis as shown in Figure 6. These elements consist of crystal units, retardation coils, adjustable air and fixed mica capacitors, and deposited carbon resistors. There are four Y-cut EDT crystal units each mounted in an evacuated sealed glass envelope. The metallic coating on each major surface of the crystal plate is divided longitudinally by a fine line to provide two identical electrical structures in each balanced crystal unit. These Y-cut units have a temperature coefficient which is zero a little above room temperature and increases to about 10 parts per million per degree Fahrenheit at the extremes of temperature encountered in the central offices. This is in contrast to the 15 parts per million for the -18 degree X-cut quartz units used in the earlier filters. The dissipation associated with the EDT crystal units is somewhat greater than that encountered in the corresponding quartz units. However, the crystal unit Q's of 15,000 to 20,000 are sufficient to provide the sharp cutoffs desired in these filters. A more detailed description of the construction and performance of these units is included in a paper[6] by J. P. Griffin and E. S. Pennell

Figure 6. Filter parts are compactly assembled and wired on a chassis, the air capacitors are adjusted, and the unit is hermetically sealed in the filter container

The growing of EDT crystal is covered in another paper[7] by A. C. Walker and G. T. Kohman.

There are three retardation coils,[8] ranging in inductance values from 15 to 40 millihenrys, used in this filter. They are toroidal coils wound on rings of molybdenum permalloy approximately one and three fourths inch in diameter. The core material has been so designed that the coils give stable performance with temperature changes and over the expected life of the apparatus. Eddy current losses in the cores are reduced by the use of very finely powdered magnetic particles in their formation. The cores are formed under extreme pressure from a mixture of this powder and insulating material. The coils are wound with finely stranded wire to secure relatively high Q's at the frequencies at which they are to operate. The coil winding is divided into four sections as shown in Figure 7. Because of the high coil inductances called for by the filter design, care is taken to make the parasitic capacitances between windings and between windings and the case as low as practicable. A molded framework placed around the core reduces the capacitance from windings to core and improves the uniformity of the sectionalized windings. This framework also locates the coil centrally in its container keeping the capacitance from windings to case uniform. The final operation in the con-

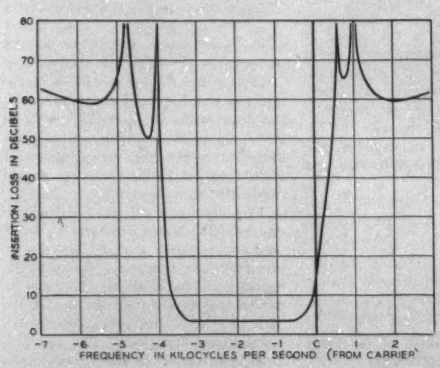

Figure 5 (left). Insertion - loss - frequency characteristics of the 12 channel filters are the same, when plotted in cycles removed from the carrier frequencies

Figure 7. Toroidally wound coil is located centrally in its hermetically sealed container

Figure 8. Two-terminal network connected across the paralleled end of the group of 12 filters to make the transmission through the two end channels of the group equal to that through the others

struction of the coil consists of vacuum drying and wax filling followed immediately by hermetic sealing. The resulting coil is highly stable, having a negative temperature coefficient of approximately 30 parts per million per degree Fahrenheit.

Three of the adjustable capacitors along with their associated molded silvered mica[9] capacitors provide the shunt capacitance at the input and output of each lattice. The other two adjustable capacitors are differential type and they provide adjustments for each of the lattice bridge circuits thereby permitting the location of two of the filter attenuation peaks at discrete frequencies.

Resistors are used to regulate the loss level so that the pass band losses of the 12 filters are approximately the same. This band loss is contributed mainly by the dissipation associated with the retardation coils and, to have equal losses in the 12 filters, the Q's of all the coils should be directly proportional to the frequency of operation. Since for economical reasons the coils were not designed to meet this restriction, resistors are added in series with the coils to regulate dissipation. The resistors for the end coil have been combined with a resistance pad, the value of which may be varied to keep the pass band loss at the desired level. The resistors are the deposited carbon type and are approximately 7/8 inch long and 7/32 inch in diameter.

It is the function of these filters to combine the modulated speech bands from 12 subscribers sets for transmission on the line and, at the receiving terminal, to separate these same bands so that they may be sent on to the proper subscribers sets. Hence, in operation these 12 filters are connected in parallel at one end. The impedance at this end of each filter is made to rise sharply in absolute value away from the filter pass band, thereby reducing the shunting effect of the paralleling filters over the band of the transmitting filter. The two series condensers at the input of the filter are included in the design for the purpose of increasing the slope of this impedance curve. In operation a two terminal network shown in Figure 8 is connected across the paralleled filters for the purpose of reducing this shunting effect over the pass bands of the two end filters of the group.

The development of EDT crystal units has provided the filter engineer with a new design tool that will widen the field of application of crystal units to filters. The lower value for the ratio of capacitances obtained with EDT crystal units, in contrast to quartz as stated in the paper by Griffin and Pennell[8] makes possible the design of crystal filters having wider pass bands. The maximum percentage band width, that is, the ratio of band width to midband frequency, obtainable with filters using quartz crystal units and condensers only is 0.72 per cent. This value is increased to 3.0 per cent when the quartz units are replaced by EDT. In order to get still wider bands it is necessary to employ coils as well as crystal units and condensers in their construction. However, the 14.0 per cent maximum possible band widths thus obtained with the quartz units is often a restriction on the use of wide band crystal filters. The 30.0 per cent possible with EDT units will open a wider field of application.

Ethylene diamine tartrate crystal filters are being used in commercial service at widely distributed locations in the Bell Telephone System as indicated on the map, Figure 9. They consist of approximately 100 models each of the twelfth channel and 56-kc filters which were constructed and put into service during the past 18 months on a trial basis. The filters were sent to the field in a large number of shipments using the same packing methods which have been used with success on quartz filters and they were

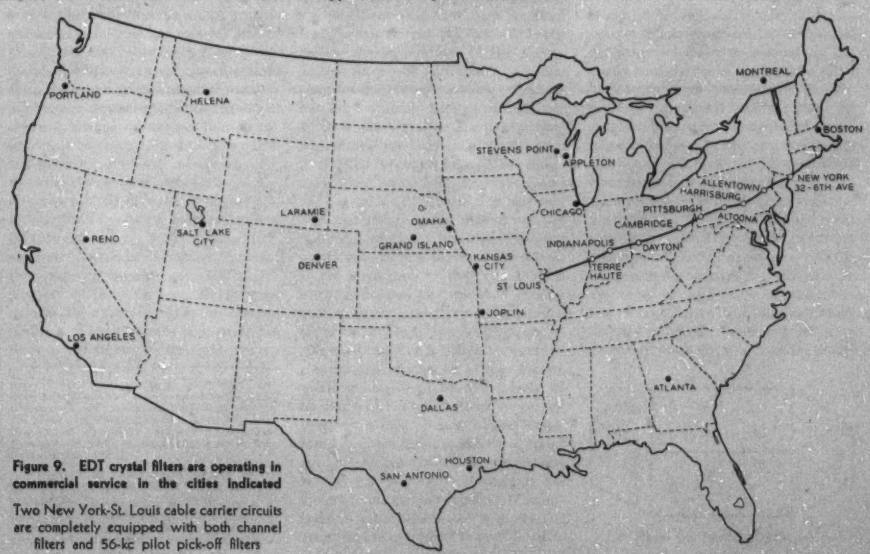

Figure 9. EDT crystal filters are operating in commercial service in the cities indicated

Two New York-St. Louis cable carrier circuits are completely equipped with both channel filters and 56-kc pilot pick-off filters

subjected to a wide variety of weather and handling conditions. The results of this trial have been most gratifying, indicating that these filters are satisfactory for commercial service and that special methods of handling will not be required.

The development of the EDT crystal unit and its application to filter circuits has provided the Bell Telephone System with an independent source of piezo-active material that is suitable for use in filter circuits. This came at a time when it appeared likely that the provision of urgently needed telephone service for the public would be restricted by the inability to import quartz in sufficient quantities to meet the needs.

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Design and Performance of Ethylene Diamine Tartrate Crystal Units

J. P. GRIFFIN
NONMEMBER AIEE

E. S. PENNELL
NONMEMBER AIEE

A COMPANION PAPER[1] describes the need for development of a substitute for quartz in the crystal units used for filters in broad-band carrier telephone systems. It was fortunate that the research work of the Laboratories[2,3,4] on piezoelectric materials, both natural and synthetic, had arrived at a stage where a suitable material was available. Such a material had to be composed of readily available raw materials, susceptible to growth in reasonable time, not too delicate to be handled, with good piezoelectric qualities and particularly to have constants which would not vary too much with temperature over the range usually encountered in telephone central offices. Quartz had admirably fulfilled all of the above conditions except procurement. With the post war expansion of needs it has become increasingly difficult to obtain quartz in adequate quantities from Brazil making it necessary to find an acceptable replacement. Rochelle salt and ammonium dihydrogen phosphate, ADP,[5] although suitable in many ways were not satisfactory from the standpoint of frequency stability with temperature changes. Two other materials, dipotassium tartrate hemihydrate, DKT, and ethylene diamine tartrate, EDT, were found in the course of Laboratories investigations to show promise of meeting the necessary requirements for crystal units. Of these two, EDT was chosen since it appeared that it could be more easily grown and processed in commercial quantities. This material was transformed from an experiment into a commercial reality during the two years 1945 and 1946. The development of this essential and valuable network element involved many unusual phases not generally associated with important inventions. The methods of producing the EDT crystal bars are discussed in a companion paper.[6]

The EDT crystals grow in the form shown in Figure 1. The controlled growing conditions that have been perfected result in clear bars up to 7 inches in length having the faces shown in the sketch. These faces are ordinarily accurate enough for reference in cutting operations but can be ground true on suitable machines if necessary after X-ray correction. The atomic spacings are different from quartz, but the copper target X-ray equipment which has been developed for quartz can be used for EDT correction purposes. The material is easily worked on belt sanding equipment; however, there is a prominent cleavage plane parallel to one of the large faces of the crystal which sometimes causes breakage when grinding thin sections. Its thermal expansion properties are such that breakage can result from rapid or localized temperature changes and crystals as well as wafered material have been cracked merely by holding with the bare hands or by placing them on a cold surface. Nevertheless, changes in temperature can be tolerated even in large sized bars if the temperature is changed uniformly throughout the material and not too rapidly. The material is water soluble and water alcohol solutions are conveniently used for etching purposes. These characteristic properties of EDT have necessitated the development of cutting and processing procedures especially adapted to this material.

Like quartz, the electrical characteristics of an EDT crystal plate vary with its angle of cut and dimensional ratios. These variations are an advantage to the filter engineer since they permit him considerable freedom in selecting configurations of network elements and crystal units. The crystal characteristics of interest in filters are the resonant frequencies, impedance level, temperature coefficients of frequency and the equivalent series and parallel capacities of the finished crystal unit. An examination of the variables entering into these attributes shows that the design of a particular crystal unit cannot be taken too casually.

Various resonances occur in a crystal plate in response to an electrical stimulus because of its piezoelectric and elastic properties. When an alternating voltage is applied to the plate an array of strains occur which are manifested as mechanical distortions of forms determined by the elastic compliances of the crystal. Two basic types of resonance are the longitudinal or extensional and the shear or rhombic distortion. Longitudinal resonances occur in combination in the length, width and thickness; shear resonances occur in the face and edge planes. Other vibrations such as flexural and torsional are excited by the longitudinal and shear piezoelectric drives with frequencies determined by the elastic compliances. All the fundamental resonances may also exist in the form of harmonics or overtones. Since a crystal may vibrate in these many different ways, the name assigned to a given unit arises not only from the angles

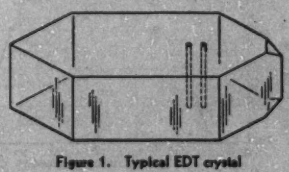

Figure 1. Typical EDT crystal

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J. P. Griffin and E. S. Pennell are with the Bell Telephone Laboratories, Murray Hill, N. J.

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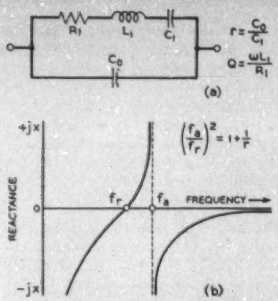


Figure 2. Equivalent circuit and reactance of a piezoelectric crystal

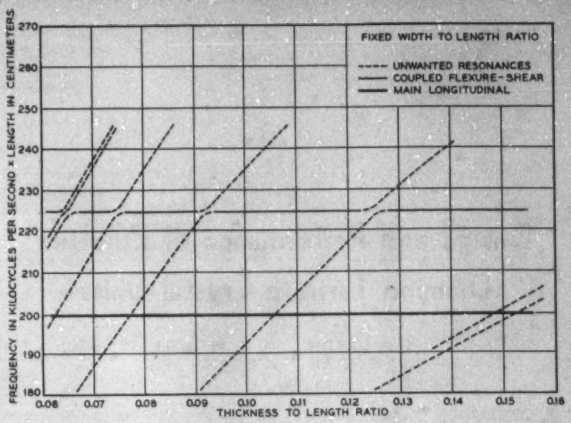


Figure 4. Unwanted resonances in EDT

of cut, but also from the fact that the crystal in question is dimensioned and mounted to best utilize a particular type of vibration. The lengthwise longitudinal resonance is usually selected for low frequency work. An example of this is the *A* cut longitudinal EDT plate used in single frequency pilot filters.

The reactance spectrum in the region of any of the several resonances of a crystal is shown in Figure 2*b* and the equivalent electrical network in Figure 2*a*. The resultant equivalent network is handled as a real but inseparable network of four elements whose values are not only known but are controllable within useful bounds.

The EDT crystals now used in the communications field are principally in the channel filters used in the broad-band carrier system filters described in a companion paper. The channel filters described are spaced at 4-kc intervals in the 60 to 108 kc frequency range and the pilot filter passes a narrow frequency band at 56 kc. Over this frequency range the longitudinal mode is utilized to assure a plate of reasonable size and shape that will provide the desired impedance level, dissipation constant, ratio of capacities, and stability when it is suitably mounted and dimensioned. Independent adjustment of frequency and series inductance L_1, Figure 2*a*, is possible in longitudinal plates since frequency is primarily a length function and inductance a thickness function. It should be pointed out that the nominal inductance of EDT is about one-third that of quartz. This is of considerable importance in filters since it leads to better impedance conditions in their design. The ratio of capacities, $r = C_0/C_1$, Figure 2*a*, is a measure of the separation of the resonant and antiresonant frequencies and may be regarded as a figure of merit in network design. The pass band width of the simplest narrow band filters is inversely related to the r of the crystal. For this reason EDT has about a four to one advantage over quartz for use in such networks and also means that for a given width-length ratio a lower impedance level can be secured. These same factors operating in wide band filter design result in band widths double those obtainable with quartz. Although the inherent Q of a crystal is very high considerable degradation results from the way in which they are necessarily mounted. The dissipation factor $Q(=\omega L/R)$ for EDT is about 30,000. This value is about half that of a comparable quartz crystal but is satisfactory for most filter use since about 10,000 is usually considered sufficient.

Temperature coefficients of frequency are related fundamentally to a variety of factors but for this purpose it is sufficient to state that in the useful cuts of EDT the shape of the frequency curve with temperature is that of a parabola concave

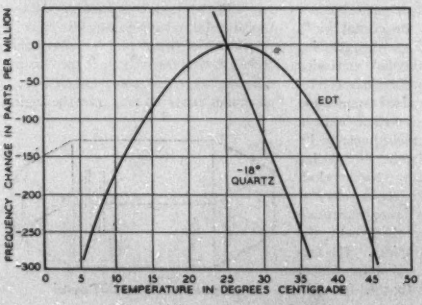


Figure 3 (left). Frequency change with temperature

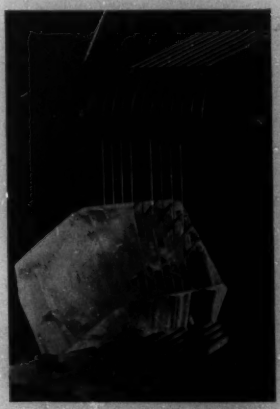
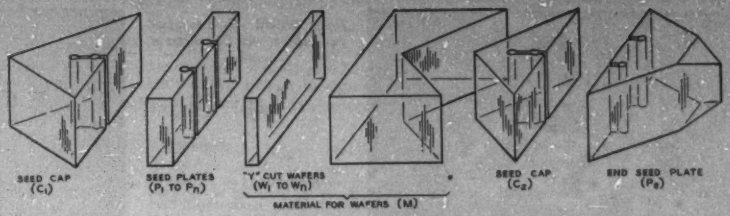


Figure 5. Cutting EDT with a wet string saw

Figure 6. EDT crystal cut to yield seeds and wafers



downward. The zero coefficient portion of the curve as shown by Figure 3 lies at about 27 degrees centigrade, the curve then slopes off to 25 parts per million per degree centigrade at 5 and 45 degrees centigrade. This figure of 25 parts per million at the extreme temperatures is about equal to the coefficient of a −18 degree quartz plate over the whole useful temperature range. Other cuts of quartz are superior in temperature coefficient but cannot be compared for low frequency use since they are not obtainable as equivalent crystals.

So far the discussion has covered only an individual resonance and its characteristics. The design problem grows more complex, however, when it is observed that every plate may vibrate in a number of different ways and in overtones thereof and that some of these modes couple with others to drive new ones. A plot of a few such resonances and how they vary with a dimensional ratio is shown in Figure 4. This plot was taken from an actual dimensioning study to find a suitable plate for filter use. The array of curves shown is only one member of a family that is dependent on width-length ratio. It can now be seen that if a crystal plate is to be selected for a band pass filter, for example, it is necessary to not only assign length, width, thickness and orientation angles to give the right fundamental frequency, inductance and ratio of capacities but coincidentally to find a combination of dimensions that provides an acceptable relative disposition of the unwanted and desired resonances. After determining the required angles and dimensions, it is necessary to prepare plates from which test models can be built and statistical data secured for establishing tolerances.

The steps in cutting EDT crystal plates for processing into crystal units differ in many ways from those for quartz. For example, to guard against heat shock the cutting operations are conducted in air conditioned work space and the temperature of the material is allowed to equalize in the room before handling. The cutting and wafering is performed by a wet string type of saw which cuts by dissolving the material locally along the cutting line. This machine is illustrated in Figure 5. The string saw consists of one or more endless strings running over pulleys to give a taut vertical section similar to a ganged band saw. The EDT crystal is fed against the moving vertical strings which are saturated by passing through a pan of temperature controlled water. A machine of this sort must be utilized since the equipment used in cutting quartz and similar hard materials generates enough heat at the cutting edges to crack EDT. The material is cut to supply seeds for replanting and wafers for subsequent processing into individual plates, as illustrated in Figure 6. After cutting the seeds are drilled to facilitate replanting in the growing tanks. The seed caps C_1 and C_2 are planted directly for growing clear crystal material. The seed plates P_1 to P_n and the end plate P_e are subjected to a capping operation to give seed caps equivalent to C_1. The usable material may be wafered for any specified orientation by using the correct type of holding fixture on the saw table. Wafering for crystal plates is done on a multistring setup which allows a bar to be completely wafered with a single pass of the saw table. A comb type of holding fixture assures the proper orientation of the bar on the saw table as the strings cut along the teeth of the comb. The resulting wafers have smooth faces and can be held to angle tolerances of approximately 30 minutes. Figure 5 shows this operation for the A cut of wafer. For the Y cut wafer the cut is made at right angles to the length of the crystal bar. The cutting rate is approximately 30 minutes per inch of feed. Where material of a size to give 10 wafers is cut on the multistring saw this corresponds to 3 minutes per wafer-inch.

Following cutting, the wafers are rough ground to give a flat face and a straight edge and then X-ray checked for face and edge orientation. Out-of-limit wafers are corrected on adjustable X-ray fixtures and then ground to give true faces and edges. The X-ray fixtures used in checking and correcting wafers are similar to those used for corresponding operations on quartz.

Wafers can be cut to approximate plate size by the simple operation of scribing to a line, with a sharp pointed scriber, and breaking in much the same manner that glass is cut.

The grinding operations are performed on a wet belt sander equipped with accurate ways and holding fixtures. To prevent local heating a liquid coolant is used. For continuous operation, cooling coils or adequate liquid capacity are needed to keep all parts of the machine within a few degrees of room temperature. Rough grinding operations are done with coarse belts of about 200 mesh. Final dimensioning, by paralleling to trued faces and edges, is done with much finer belts. To avoid breakage of the material, a belt is needed which is free from appreciable thickness variation at the joint. These grinding operations are capable of

Figure 7. Typical EDT crystal unit

Figure 8 (left). Gold evaporation chamber with EDT plates in position

Figure 9. Fastax camera shot of EDT breaking during impact test

producing finished crystal plates to orientation tolerances of 30 minutes and dimensional tolerances of 2 mils.

Processing the plates into finished crystal units shown in Figure 7 now begins. The first steps consist of etching the plates in a solution of alcohol and water and attachment of four-headed terminal wires by a cementing process. These wires are only 8 mils in diameter yet can sustain a load of about 7 to 8 pounds. They are located in pairs on opposite sides near the center of the plate. Each wire attachment is subjected to a 500-gram static pull test. It is interesting to note that although the EDT material is weak, ultimate strengths of the wire to plate attachment of 3,000 to 4,000 pounds per square inch have been recorded.

The plate, with wires attached, is coated with a thin film of gold only a few hundred thousandths of an inch thick by an evaporation process in the vacuum chamber illustrated by Figure 8. A notable feature of this process is suspension of the crystals by their wires to permit simultaneous plating of both sides and to avoid unequalized temperatures. By this means the 180 degree centigrade level required for degassing is permissible although such temperatures would otherwise result in cracks and wire attachment failures. Care must be taken to insure electrical continuity from this electrode plating to the terminal wires. Gold is unavoidably deposited on all surfaces of the plate, therefore, the four edges must later be stripped of gold to insulate from each other the electrodes on the two faces. Although the plating appears dense and impervious it can easily be removed by a single pass on a dampened cloth. On some plates, where it is desired to have two separate electrodes on each major surface, the coating is divided along the lengthwise centerline. A small solder weight is attached to each terminal wire at an odd quarter wave length point[7] along its length. This reduces transmission of energy from the vibrating plate through the terminal wires to the supporting cage where it would be dissipated with resultant degradation in Q.

Frequency adjustment is accomplished by reducing the length of the plate—initially dimensioned somewhat longer than the theoretical correct length—by grinding the end of the plate by hand or machine on a fine dry abrasive paper. After adjustment, the crystal plate is mounted in a suitable cage. For this operation the plate is temporarily held in position by means of paper shims while the terminal wires are soldered to the uprights. The shims are then removed, leaving the plate centrally located and supported by its four fine wires. Movement of the plate is restricted to about 25 mils by the use of suitable bumpers that are an integral part of the cage. A complete description of similar techniques used in fabricating quartz crystal units is given by R. M. C. Greenidge.[8]

The cage assembly is sealed in a standard glass vacuum tube bulb and then evacuated and based to result in the completed EDT crystal unit which is then subjected to final tests.

Since EDT is such a new and vulnerable material, fairly elaborate testing has been going on continuously since early 1946 to help devise improved plate mountings that insure safe shipment and to determine the practical limitations imposed by the coefficients of thermal expansion. This testing program in the past twelve months has involved over 100,000 individual measurements of temperature coefficients, mechanical and thermal shock effects, vibration, aging and current capacity. These measurements show that a typical EDT crystal unit will frequency age about +40 parts per million during the first year. This aging rate is rapid at first but diminishes to almost zero at the end of the year. The same general sort of thing occurs for the dissipation factor, the resistance decreasing to 50 per cent of its initial level and becoming asymptotic within a year. In frequency aging the dispersion is low enough so that the ultimate frequency can be predicted and provided for during final adjustment.

In the early stages of development the fragility of the EDT material gave rise to considerable uncertainty with regard to whether or not the completed units would withstand the shocks encountered during normal handling and shipping. Hence, attention was centered on shock testing and development of suitable plate mounting methods. The photograph of Figure 9 (a single frame from a Fastax camera film travelling at the rate of 8,000 frames per second) shows a crystal rupturing from the indirect impact of a 500-pound weight dropped four feet. A mathematical examination of the displacements observed in a series of these films shows that the crystal plate as now mounted will break at 1,000g but easily withstand 500g. The complete crystal unit, however, will develop a failure of one sort or another such as chips, cracks, or wire attachment failure at about 120g. This compares very favorably with vacuum tubes and shows that the crystals will stand the handling incident to processing and during shipment. Crystals used in telephonic communication networks occasionally are subjected to overload currents when trouble conditions arise. Tests show that the EDT crystals, like equivalent quartz, will handle about 3 milli-

watts of power without injury. Injury in this sense consists of failure of the cemented wire attachment or fracture of the plate itself. It is interesting to note that in the event of crystal plate failure due to overload the successive phases are: surface ionization, heat expansion, then rupture.

In comparing the characteristics of the new EDT synthetic crystal units to the more commonly used cuts of quartz for network applications in the low frequency ranges it may be said that EDT is superior with regard to inductance and ratio of capacities, that it is approximately equal or superior in temperature coefficient, physical size and aging characteristics, but that it is inferior in regard to mechanical strength and dissipation factor.

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No Discussion

The Heat Pump From the Utility's Point of View

S. W. ANDREWS
ASSOCIATE AIEE

THE ELECTRIC utility industry is intensely interested in the heat pump because it holds promise of permitting the industry to supply domestic-heating service in an economical manner. Up until now the electric utility industry has supplied very little of this market and we think that without the heat pump we are not likely ever to supply very much of it.

The market, however, is a big one. It has been estimated that the heat required for domestic house heating, if translated into kilowatt-hours of electric energy, would amount to 700 billion, as compared with the total sales of electric energy in the year 1945 to residential customers of 34 billion kilowatt-hours. A market of this size naturally holds out great attractions to our industry if it can be developed in a manner which will be advantageous to the supplying companies, to the manufacturers of equipment, and to the customers themselves.

I should like to discuss with you the matter of the heat pump from three points of view.

1. Why have the electric utilities been unable to serve much of the domestic heating market in years past and why do we say it is unlikely they can ever do so without the heat pump? We think that there are compelling reasons as to why this is the case which I shall try to develop briefly.

2. Why do we think that the heat pump changes the picture and makes it possible for the electric utility industry to go after the domestic heating market? Again there are very compelling reasons as to why this is so, provided that necessary developments can be accomplished to give an economical result.

3. What is the present status of the heat pump? What has been done to date? What further needs to be done? There has been a great deal of publicity in recent months about the heat pump. How close is it to being commercially available?

To date the only available means of heating buildings by electricity is with a resistance-type heater. This may take the form of the small familiar socket appliance with a reflector; a unit built into the wall of a room; an electrically heated steam or hot-water radiator; a panel-type radiant-heating system, or central-heating units which are installed in a duct system supplying an entire building.

In any of these cases, the electric energy is transformed to heat energy by resistance heaters. Any one of these arrangements may be low in first cost from the customer's point of view, but the cost of supplying electric energy to a resistance-type heater used for space heating is high.

If an electric utility company generates most of its energy from fuel, the basic source of heat liberated in the house by a resistance heater is the fuel which is burned in the power plant. Even in the most efficient modern fuel-burning generating stations, we can convert into electric energy only about one-third of the heat energy in the fuel which is burned in the boilers. This does not compare with the fuel-burning efficiencies which can be achieved in the customer's premises. With a modern fuel-burning system, he can achieve a fuel efficiency of 50 to 70 per cent without too much difficulty and thus, from the point of view of fuel efficiency, electric heating with resistance heaters has a basic disadvantage not readily to be overcome.

Regardless of whether the utility company generates its energy from fuel or from water, there is another difficulty in supplying electric heat by space heaters. The annual load factor of heating load is very poor and has been estimated at from 10 to 20 per cent in different parts of the country depending in general on the latitude and, therefore, on the amount of heating service to be supplied. Low load-factor business considered by itself is very costly for an electric utility to furnish because of the heavy fixed charges which have to be met on the facilities necessary to supply the service. It is interesting to note that in the State of Washington, the municipal utilities at Seattle and Tacoma, both supplied largely by hydro energy, recently have found it necessary to institute higher rates for house heating by resistance units than for other types of residential service.

It is our belief that for these reasons electric house heating by space heaters is not likely to be widely attractive to customers, because the customer will probably always have to pay substantially more for the electric service than for some other competitive form of heat, without any comparable advantages to offset the extra cost.

Before answering the second question, perhaps we should describe very briefly what the heat pump is. This has been covered in many articles in the technical press and in various books, and I think it needs just a brief word here.

Basically, the heat pump is an electrically-driven compression-type refrigeration machine. During the heating season it is arranged to take heat from a low temperature source outside the building to be heated, raise the temperature of the heat, and release it in the building. During the cooling cycle the operation of the unit is reversed so that heat can be taken from the building and released outside. This can be accomplished either by reversing the flow of the refrigerant, thus changing the condenser to an evaporator and the evaporator to the condenser, or it can be accomplished by changing the

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S. W. ANDREWS is rate engineer with the American Gas and Electric Service Corporation, New York, N. Y.

flow of the medium, air or water, which is used to carry heat into or out of the building and release it in the house, so that the medium which releases heat in the house by flowing over the condenser during the heating season is redirected to flow over the evaporator during the cooling season.

In considering the heating cycle, heat pumps may be arranged to take heat from the outside atmosphere, from a well, from ground water, from a city water supply, or from the ground itself. Heat may be released in the house in the form of hot air or hot water, but with present types of equipment it may not be released in the form of steam, because temperatures high enough to generate steam cannot be achieved economically on the discharge side of the unit.

Whichever system is used, they each have their advantages and limitations. The important thing to consider is that the unit takes heat already in existence and transfers it from the outside of the house to the inside. It therefore results that the electric energy which drives the machine does not have to supply all or even a major part of the heat which is liberated inside the house. When you compare the amount of heat which is liberated in the house with the heat equivalent of the electric energy used to drive the machine, you have a relationship known as the coefficient of performance. This is always a figure greater than one and it will vary from two, to as high as six or seven, depending upon the temperature of the heat source and the temperature at which heat is liberated. The closer these two temperatures are, the higher the coefficient of performance and, conversely, the greater the difference between them, the lower the coefficient of performance. When these two figures, that is the temperature of the heat source and the temperature at which heat is liberated, vary to a considerable degree, as they will in many installations at different times in the year, the coefficient of performance of the unit will likewise vary.

Let us go back to our discussion of the fuel efficiency involved in the resistance-type heaters. We said that the best efficiency which could be achieved in a modern fuel-burning generating system was about one-third. If we have a heat-pump installation with a coefficient of performance of three to one, which is an average figure that we think can be materially bettered, it results that the overall performance of the heat-pump unit is about equivalent to a fuel efficiency of one. In other words, by taking two-thirds of the heat from the source outside the house and one-third from the electric energy used to drive the machine, we end up by putting into the house just about the same amount of heat which was available in the fuel which we burned in the generating plant to produce the energy necessary to drive the heat pump. Our competitive situation is very materially improved as compared to what it is with the resistance-type heater.

Add to this the fact that the heat pump with a coefficient of performance of three to one takes only one-third as much electric energy and creates an electrical demand only one-third as great as would a resistance heater. This means that the utility company has to supply very much less in the way of generating equipment and transmission and distribution system facilities for the heat pump than for the resistance-type heater; and therefore, fixed charges, the operating costs, and the losses are very much less.

Furthermore, inasmuch as the heat pump can be reversed and can operate to cool a house, it is a unit which operates 12 months in the year instead of just a few months during the heating season. The annual load factor is greatly improved and is perhaps brought up to figures of 25 to 35 per cent in various parts of the country and the cost of energy to be delivered to the customer is thus still further reduced.

Many of us in the electric utility industry, accordingly, think that when suitable heat-pump units are on the market, and when all the various application problems are solved, we will have a tool to use in going after the domestic heating and cooling market, which will put us in a very competitive position and will permit us to serve our proper share of this important market.

Question three was the present status of the heat pump, what has been done and what further needs to be done. Let us discuss briefly these questions from two points of view. The first is the development of equipment. The second is the determination of the best heat source to be used in any installation.

There are a considerable number of heat-pump units in operation both in the United States and in parts of Europe. Up until the time of the war, most of the installations were custom-built. They were mostly installed in buildings larger than residences and they were specially designed and engineered for a particular job. For the most part they took existing component parts and brought them together into workable systems. In the American Gas and Electric System, we have eight such installations, the earliest of which goes back to 1934. These installations are used in office buildings and substations on our system. We have treated each one as very much of an experiment, and have made a variety of tests and kept performance records. Our results heretofore have been widely published.

Since the war, there have been a few manufacturers who have built some units for domestic purposes. These, to a considerable extent, have been simply assembled jobs in which the manufacturer brought together the component parts which he could buy on the market, or some of which he may have made himself. There are not many of them actually in service, perhaps 200 or 300, but they have been treated to some extent as developmental units.

In the spring of 1946, the American Gas and Electric System believed that units could be built which would give better performance and better reliability of service than any which were then available. We accordingly developed, after consultation with many utility companies throughout the country, specifications calling for 3-horsepower single-phase units, single phase because this is ordinarily the only type of electric service available to residential customers. We have invited a number of manufacturers to build units to these specifications and to meet the standards of performance set forth therein. We had in mind that units specially designed and specially engineered for household heating could give high coefficients of performance and could be reasonably trouble free as is the familiar electric refrigerator. Several manufacturers are now working on this problem.

In discussing present status, another phase which I should like to cover briefly is the matter of the determination of the heat source. In many ways you have the simplest installation, and unquestionably the cheapest, from the point of view of first cost, if you take heat from the outside atmosphere. Such a system is especially attractive in latitudes where the outside temperature never goes below, let us say, 30 degrees. The average temperature will be higher than this and the coefficient of performance will be good. An air system has its limitations, however, because the colder it gets outside and the more heat you need to pump into the house, the lower becomes the coefficient of performance and the less the heat output of the machine. A unit taking heat from the outside air, and located in the northern part of the country, might have to be unduly large in size and thus

uneconomic from the first-cost point of view.

A water source of heat has some attractions, but also limitations. If there were ample supplies of ground water and no great difficulties in handling the discharge from the well, this would be attractive because ground water maintains a fairly constant temperature of 40 to 60 degrees throughout the year in many parts of the country. The use of city water would ordinarily be too expensive because of the large quantity which would be required. There are many places in the country where water from any source is not available in the quantities which would be required.

A third source of heat is the ground itself. The ground, like ground water, maintains fairly stable temperatures throughout the country when you get down below frost lines. It would appear that this is the best source of heat for the northern parts of the country and much work is now being done by various groups, within and without the utility industry, on studying this problem to find out how rapidly heat can be taken out of the ground without unduly lowering the ground temperature in the immediate vicinity of the building to be heated and to find out the design and type of equipment best suited for obtaining this heat. The electric utility industry has recently set up a co-ordinating committee with representatives from companies in all parts of the country, as well as representation from both the Edison Electric Institute and Association of Edison Illuminating Companies. This committee has been formed to bring together the results of various research projects and to suggest lines of further research to interested companies and groups.

In closing this short, general summary of the heat pump from the utility's point of view, I should like to express the opinion that the heat pump is definitely beyond the experimental stage, but just as definitely still in the developmental stage. There is a lot of work still to be done before we get to the day when Mr. or Mrs. Customer can walk into a heating contractor's establishment and buy the right kind of unit for his own house with every assurance that it will perform as advertised.

Since we are still in the developmental stages, and no units have yet been built in large quantities with mass production techniques, it is impossible for me to give you any real idea as to what heat pump units for the home are likely to cost. It would seem, however, that the cost of a heat-pump unit should be less than a separate fuel-fired heating system plus a conventional cooling system.

I mentioned earlier that electric rates for house heating by space heaters had been increased recently in the State of Washington. Since we are in the developmental stage with respect to the heat pump, and particularly since we are in a period in which the cost of just about everything we buy is rising, it is likewise impossible to venture a guess as to what ultimate electric rates would be for service to heat pumps. In the case of my own company, and I think this goes for many other utility companies, it is our belief that initially, at least, electric service can be supplied for heat-pump installations under the standard rates available for the customer's regular service. As time goes on and more experience is gained, electric utilities will naturally review the load characteristics of house-heating service and will relate these to the cost of supplying the service. I am sure that electric rates for house-heating service will find their own proper level, just as has been the case in years gone by in the supplying of service to electric ranges, refrigerators, water heaters, and the many other types of load which are supplied by electric service.

No Discussion

Growing Crystals of Ethylene Diamine Tartrate

A. C. WALKER
NONMEMBER AIEE

G. T. KOHMAN
NONMEMBER AIEE

THE NEED for a crystal, preferably one which can be artificially grown, to relieve the critical quartz supply situation has been adequately emphasized in the companion papers by Willis,[1] and Griffin[2] and Pennell. Fortunately this need was foreseen and research activity in the Bell Telephone Laboratories has been intensified over the past ten years* with the objective of finding a satisfactory substitute for quartz for use in circuit elements. More than a hundred materials have been explored in this investigation and the work in this field continues.

Out of this work there emerged, a few years ago, two materials, ethylene diamine tartrate and dipotassium tartrate hemihydrate offering especially valuable promise as piezoelectric circuit elements.[3] Both crystals afford modes of vibration the frequencies of which are substantially independent of temperature and which have attractively high electromechanical coupling coefficients. Ethylene diamine tartrate, referred to hereafter as EDT, was selected for more intensive study as a circuit element because of certain advantages in physical properties. The purpose of this paper is to discuss methods of growing crystals particularly as they are employed in growing large crystals of EDT.

Preliminary measurements to detect piezoelectric properties in crystals are made by methods described by Mason,[4] and for this purpose only small crystals are needed, such as those which spontaneously form when a solution of the salt is saturated at elevated temperature and then cooled to room conditions, or when solvent is evaporated from a saturated solution. For quantitative measurements and for use in experimental apparatus, in accordance with the methods described[1,2] it is necessary to grow crystals of sufficient size so that plates may be obtained of the desired orientation and dimensions.

There are two general methods of growing large clear crystals from solution.[5] These are (1) by gradual reduction of temperature of a solution containing suitable seeds and saturated at elevated temperature, and (2) by feeding a solution saturated at one temperature into a tank containing seed crystals kept at a constant temperature slightly lower than the saturation temperature. In the case of each of these methods flawless growth depends upon supplying salt to the growing face uniformly, which is accomplished by careful control of the degree of supersaturation and by controlled circulation of the supersaturated solution around the growing crystal. The supersaturation is obtained by sensitive and reliable methods of temperature control. Circulation of the growing solution is usually obtained by gentle rocking of a tank[6] in which the seed is planted as shown in the photograph of the rocking tank method, Figure 1, or by the reciprocating motion of a spider to which the crystal seeds are attached in the growing solution as shown in the photograph of the rotary crystallizer,[6] Figure 2.

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A. C. WALKER and G. T. KOHMAN are with the Bell Telephone Laboratories, Inc., Murray Hill, N. J.

The rocking tank method which has been successfully applied to the growing of Rochelle salt and ammonium dihydrogen phosphate is not suitable for the growing of crystals of EDT because of a much greater tendency to crystallize from solution spontaneously as compared with the previously mentioned crystals. To avoid interference by spontaneously formed crystals, the rotary method† shown in Figure 2 was developed. Crystal seeds are mounted on a spider rotated with a reciprocating motion through the saturated solution by the motor drive on top of the crystallizer. This motion tends to sweep spurious seeds from the growing faces permitting them to settle on the bottom of the tank where an unsaturated zone is maintained because of the proximity of the tank bottom to the heating element located in the base of the apparatus. A second unsaturated zone is maintained in the top surface layer because water evaporating from the surface condenses on the crystallizer cover as shown, and drips back into the surface layer of the solution. A minimum of circulation in this layer aids in establishing an equilibrium condition of some dilution, thus preventing the spontaneous growth of seeds in this zone.

Although the growing of large crystals is a highly developed industrial process, there is a surprising lack of quantitative information concerning the factors which control crystal growth. It is well known that the presence of traces of certain impurities can distinctly alter crystal habit as shown by the work of W. G. France[7] and as illustrated by the crystals of ammonium dihydrogen phosphate shown in Figure 3. The tapering illustrated by crystal *C* is the result of the presence of 0.001 mole of chromium per liter of solution. As little as 0.0002 mole of chromium produces a detectable effect as shown by crystal *B* as compared with crystal *A* grown from a chromium-free solution.** This effect is probably caused by selective adsorption of ions on

* The survey of materials and the growth of the initial crystals for research investigations has been in the hands of A. N. Holden. Crystallographic aspects of the problem have been handled by W. L. Bond, and the electrical and elastic measurements and their interpretation by W. P. Mason.

† Developed by A. N. Holden.

** The effect of chromium on the growth of ammonium dihydrogen phosphate was first observed by H. Jaffe of the Brush Development Company.

Figure 1 (left). Rocking tank method

Figure 2 (above). The rotary crystallizer

certain crystal faces which alters their relative growth rates.

Viscosity is an extremely important factor in crystal growth as illustrated by the devitrification of glass, which term is used to describe the very slow conversion of this supercooled, highly viscous liquid into a crystalline solid. In contrast, the rate of growth of crystals is very fast from an aqueous salt solution, even one of relatively high viscosity such as ethylene diamine tartrate. Because of the intimate and complicated relationship between temperature, solubility and viscosity, it is difficult to predict the influence of temperature on rate of crystal growth. Temperature controls both solubility and viscosity but since viscosity normally decreases with temperature and increases with solubility there are two opposing influences on crystal growth. As a result rate of crystal growth may pass through a maximum with increasing temperature. One of the common methods of altering crystal habit is by control of hydrogen-ion concentration. This effect is of great value in controlling tapering effects. The effect of degree of supersaturation has been studied by H. E. Buckley[8] who found that new faces can be made to appear and disappear by varying this factor. This effect is evidence of a different rate dependence on supersaturation for the various crystal faces.

The first step in the growing of EDT crystals is the preparation of the salt, which until recently was only a laboratory curiosity. The salt was first prepared by Colson in France in 1892,[9] but sizable quantities of it were first prepared in the Bell Telephone Laboratories in 1945 for use in growing large EDT crystals weighing approximately one pound each. The salt is prepared by adding solid crystalline tartaric acid to a solution of ethylene

Figure 3. Effect of chromium on crystal habit

A. No chromium
B. 0.0002 mole chromium per liter
C. 0.001 mole chromium per liter

Figure 4. EDT crystals

1. Crystals formed spontaneously
2. Seed plate cut from crystal
3. Seed cap cut from crystal
4. EDT crystal bar
5. Seed formed by capping plate

Figure 5 (right). Seeds mounted on spider

diamine in water. Because considerable heat is evolved on mixing the acid and the basic ethylene diamine it is necessary to provide cooling during this operation. Despite this some slight decomposition occurs and the mixture is colored more or less depending upon the effectiveness of the cooling. Most of this decomposed brownish colored material may be removed by stirring with decolorizing charcoal and filtering. The salt is next separated in the form of masses of crystals such as those shown in Figure 4, number 1, by preparing a solution of the salt saturated at some elevated temperature and allowing to stand in a cool place for some days. The larger and better formed crystals in such a mass are selected as seeds and planted in a solution saturated at some fixed temperature between 40 and 50 degrees centigrade. Saturation is accomplished by vigorously stirring the solution at this fixed temperature in the presence of an excess of finely divided salt for a period of approximately ten hours. The excess solid salt is then allowed to settle and the clear liquid pumped off. This solution is heated above the saturation point to insure complete solution of chance crystals. In the absence of such nuclei it is possible to supercool a solution of EDT salt by as much as 10 degrees centigrade, without danger of crystallization. It is this property of supercooling which makes it possible to grow large crystals from solution.

Seeds planted in a supercooled solution will grow at a rate dependent upon the amount of such supercooling (supersaturation), temperature, and circulation. At extreme supersaturation, growth becomes uneven and veils occur, or spurious seeds form and are adsorbed on crystal faces where circulation is poorest, inducing abnormal growths at such points. It is an interesting fact that crystals containing imperfections or abnormalities grow more rapidly than perfect crystals, and crystal growth thus bears some resemblance to the physiological growth of cancerous tissue.

Under optimum growing conditions growth takes place almost entirely on the two natural faces which compose the wedge-shaped left end of the crystal in

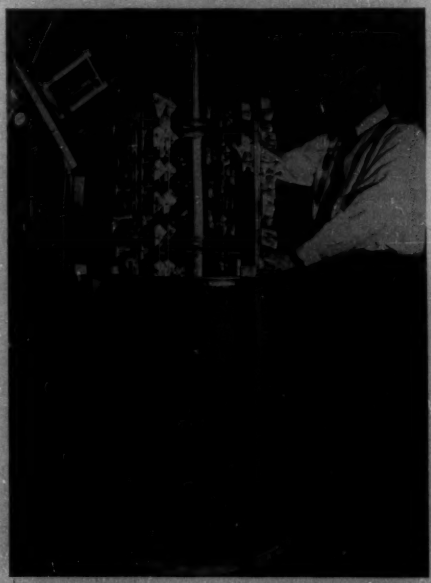

Figure 6. Inserting spider in growing tank

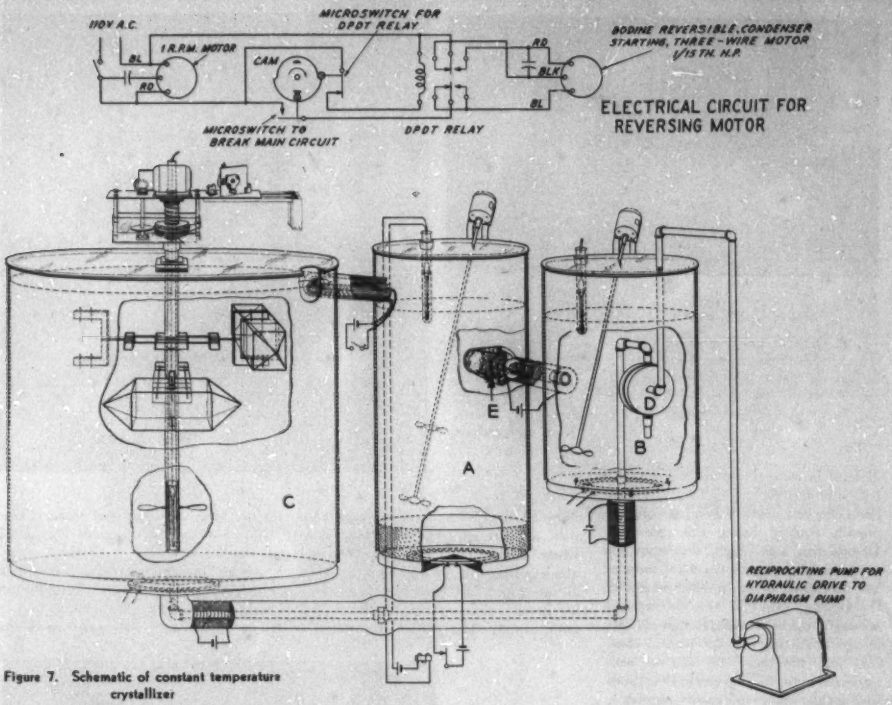

Figure 7. Schematic of constant temperature crystallizer

Figure 4, number 3. Thus small perfect seeds will grow into long needle-like crystals with cross sections very little larger than those of the original seeds. The abnormalities resulting from adsorption of spurious seeds on nongrowing faces is of great advantage in providing a means of enlarging the crystal cross section, an otherwise slow and tedious process.

The fastidious nature of crystals is well illustrated by their reluctance to grow clear material on faces other than the natural ones.* An artificial face, even when off angle by only a fraction of a degree must first be covered with faulty, veiled growth until the plane of the natural face is reached. This accounts for the cloudy (veiled) portion observed in the initial growth section of EDT crystals formed from a cut section, and is the reason for the fact that the growing process involves two operations. These are: (1) capping—the formation of the natural faces or caps on a cut plate of the proper cross-section; and (2) bar growth—the formation of clear material on the seed cap cut from the wedge-shaped end of a capped seed plate. In Figure 4, number 2 is such a seed plate, number 5 a capped seed plate, and number 3 the useful growing end of the capped seed plate which forms the seed for growing the final bar, number 4.

Plate seeds are usually cut thick enough to permit the drilling of mounting holes, arranged so that the plates may be mounted on the spider shown in Figure 5, and oriented for solution flow across the major cut surfaces. Growth takes place on both these cut surfaces, accompanied by marked veiling, until all of the natural faces are formed on each side. Such capped seed plates are then harvested and the wedge end containing the two growing faces is cut off, drilled for mounting on the spider in Figure 5 in such a way that the solution flow is against these growing faces.† A spider, fully mounted with these caps is being lowered into the growing tank in Figure 6. When in place, the tank is equipped for operation and placed in a room heated to about 50 degrees centigrade. Solution saturated at 45 degrees centigrade, is pumped into the tank at a temperature four to five degrees above the saturation point. The filled tank is then held at this temperature for several hours with agitation of the spider to dissolve any crystal fragments adhering to the crystal faces, and when the growing surfaces of the crystals are quite clean of misoriented material the temperature of the room is brought to 25 degrees centigrade, and that of the solution is slowly dropped over a period of several

* The usual natural faces on EDT crystals are those forming the four prism sides of the bar, the two faces forming the wedge-like tip on the clear end of the bar (the growing end), and the faces comprising the opposite end along the growing or Y-axis. This latter end does not grow appreciably after the natural faces are formed, and these faces may appear either as a wedge (see right end of the bar crystal number 4 in Figure 4), or as four faces converging to a pyramid (see the beginning of this pyramid on the right end of the capped seed plate number 5, Figure 4). Other faces sometimes appear, depending upon impurities, but little is known as yet regarding this aspect of the EDT crystal habit.

† Once sufficient numbers of good bars are available the clear cap ends are cut off and used as seeds for growing more bars, and the capping operation is no longer necessary.

hours until growing commences. After this the solution is cooled a few tenths of a degree per day, which causes the crystals to grow approximately one inch per month along the growing direction. During the early part of this growing period the cut face of the wedge fills out to the natural, nongrowing faces, and thereafter substantially no growth takes place on these wedge or pyramid faces.

For large scale production, a constant temperature method has a number of important advantages. The control of temperature at a fixed point does not require the complicated programming controls needed in connection with the dropping temperature process. Growth rates are also under better control and the total amount of growth obtainable is not dependent upon the quantity of salt contained initially in the solution, as will become clear later.

For pilot plant production the constant temperature apparatus, shown schematically in Figure 7, was constructed and is shown in Figure 8. The unit consists of three interconnected tanks, *A*, *B*, and *C*, see Figure 7, each provided with separate, relatively simple temperature controls. Seeds are mounted as previously described on a spider in *C*, and the system filled with saturated salt solution. Excess salt is added, as needed, in granular form to tank *A*, and this is kept thoroughly mixed with the solution in this tank by means of an efficient stirrer. The temperature of tank *C* is held at the growing value, that of tank *A* at a slightly higher temperature, and because of the presence of solid salt in this tank, this temperature is the saturation point. Tank *B*, termed the superheater, is kept at a still higher temperature than *A* to prevent the formation of spontaneous seeds in the solution pumped into the bottom of *C* by means of the pump *D* in tank *B*. As this heated solution flows into the bottom of *C* it quickly cools to the growing temperature as it rises in the tank and the excess salt corresponding to the difference in solubilities at the temperatures in tanks *A* and *C*, supplies the material for crystal growth. The overflow from tank *C*, from which much of this excess salt has been removed by the growth of the crystals, returns again to tank *A* where it is resaturated at the higher temperature.

Solution flows from tank *A* into *B* by gravity, through a filter *E*, which removes suspended crystals. The superheater tank *B* insures that any crystalline material passing through the filter will be dissolved before it reaches the pump. This cycle can be continued as long as there is room for the crystals to grow in *C*, by adding salt to the saturator tank *A* as required. This system is extremely flexible as it readily permits adjustments of temperature, rates of flow, and solution composition during the course of a run.

Figure 8. Constant temperature crystallizer

It is desirable to grow EDT crystals at a temperature just above 40 degrees centigrade. As heat currently added to the solution in the tanks during the growing process must be dissipated, the entire apparatus is enclosed in a space provided with some refrigeration and temperature control. In harvesting the crop of crystals the temperature of this space is brought to ambient and the solution pumped from the tanks to storage containers. The crystals are then allowed to remain undisturbed for a period of from 24 to 48 hours before they are removed from the spider. This permits temperature equalization and drainage of solution from the crystal surfaces. Also there is some evidence that an annealing action may be involved. The crystals are then removed, allowed to drain on dry cloth covered tables to remove solution trapped in the porous area within the veiled central section. Finally they are placed in individual cellophane bags to protect them from mechanical and thermal shock while being transported for processing by methods described by Griffin and Pennell.[2]

In addition to the electrical characteristics described by Mason,[5,6] EDT crystals possess certain peculiar growing habits. As noted previously for reasons not understood, EDT grows only in one direction under the conditions here described. Therefore, when a plate is cut from a bar some means must be provided for identifying the surfaces on which form the growing and nongrowing cap faces, so that when mounted on the spider, interference will not occur during growth.

A second and troublesome characteristic of this crystal is its tendency to crack. There are large temperature coefficients of expansion in the crystal and when the temperature of the surface of the crystal is suddenly changed from that of the main body, cracks are likely to occur. This tendency necessitates the use of special cutting techniques described by Griffin and Pennell.[2]

A third characteristic is the tendency for new faces to appear or disappear during growth, probably resulting from some changes in growing conditions, such as hydrogen-ion concentration and the introduction of impurities from a variety of sources.

The pilot plant stage in the growing process previously described has been of great value in the development of the manufacturing process. It has been possible to increase growing capacity by the use of as many as six growing tanks coupled to each saturator and superheater unit. The knowledge gained through operation of the pilot plant has made it possible to foresee difficulties in produc-

tion and to trace causes of faulty growth in time to prevent serious loss of time and material. The capacity of the Western Electric plant to produce EDT crystals for use as a quartz substitute is expected to go far in reducing the shortage of natural quartz, especially of the larger sizes required for filter applications. Improvements are in progress which should approximately double the percentage of usable material per pound of crystal as compared with that for natural quartz. It is therefore expected that in addition to conserving quartz for military and other applications this development will provide a reliable domestic source of supply of crystals for long distance carrier telephone systems.

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Discussion

W. L. Casper (Bell Telephone Laboratories, Incorporated, New York, N. Y.): The development of synthetic crystals was undertaken because of a shortage of quartz crystals suitable for use in carrier systems. The situation before the War had given no indication that crystals could not be obtained in any quantity that might be needed for the expansion of the telephone plant. The War caused both a complete dislocation in supply and demand and an unprecedented need for additional telephone service.

Crystal units used in carrier systems, in general, operate at frequencies from 4 to 150 kilocycles. Many of them have large plates compared with those operating at radio frequencies and must be cut from relatively large crystals. They are used for the following four purposes requiring progressively larger amounts of quartz:

1. Generation of the basic frequency from which the carrier frequencies are derived.

2. Selection from the basic frequency of the harmonics required for the carriers.

3. Picking off of certain frequencies from the line which are used in the regulation of the circuit.

4. Separation of speech or program channels, or groups of channels.

Quartz not only simplifies the regulation problem of carrier systems, but enables very efficient use to be made of the frequency spectrum available in providing a large number of channels each giving a high grade of telephone quality.

Quartz for these units is obtained from Brazil and before the War the 5,000 to 7,000 pounds needed annually were easily procured. A stockpile large enough to last several years was maintained to guard against periods when the supply might possibly be shut off. Japan imported large quantities of the smaller crystals for use in the manufacture of cheap jewelry. As only a few per cent of the quartz crystals mined were large enough for carrier circuit crystals, the sales to Japan were an important factor in enabling the mines to operate.

When the War came along the Armed Forces decided to use quartz for radio oscillators to obtain ease of handling and maximum reliability of the radio transmission. Many radio sets required 100 crystal units or more per set, and the demand skyrocketed. The Government instituted an extensive program to obtain the needed quartz from Brazil, and more than 100 shops were set up to manufacture quartz units. While most needs of the Army Forces could be taken care of by the smaller quartz crystals—too small for the lower frequency carrier crystal units—many manufacturers preferred to cut the larger quartz crystals and many were used in this way. Some War projects also required even larger quartz plates than are used in carrier circuits.

During the War only such carrier systems were manufactured and installed as were essential to the War effort. All procurement was under priority control. The quartz required for this production was allocated by the Government. Stockpiling by the Bell Telephone System for future telephone business was out of the question, although it was increasingly evident as the War progressed that a large demand for telephone service was building up.

The end of the War left the telephone companies with a large number of potential customers asking for telephone service. To give them this service, it was not only necessary to provide them with station sets for their homes, but also to add to central office equipment to handle the increased traffic and to add substantially to the long distance plant. To meet this demand for service, it was necessary to increase production of long haul carrier systems to ten times what it had been before the War.

While at V-J Day there was a sizeable supply of quartz crystals in the country, these were mainly small crystals, and the available supply of large crystals was wholly inadequate. Japan was no longer in a position to import the smaller crystals nor was it necessary for the Armed Forces to continue their purchases for War purposes. With practically no demand for the smaller crystals, it looked extremely doubtful whether mining for the larger size alone would be sufficiently profitable for operations to continue. The expectation of obtaining the quantity needed was decidedly bleak.

Fortunately, a research program on piezoelectric crystals had been in progress for a considerable period. The crystals of all known minerals which had any possibility of being useful had been investigated and the program had been extended into the synthetic field. These studies had produced two possible synthetic crystals. Of these, ethylene diamine tartrate (EDT) was chosen as giving most promise.

It was not only necessary to develop methods of growing this crystal in quantity, but crystal units had to be designed. This crystal has very different physical characteristics from quartz and many of the methods of fabrication that were used with quartz had to be modified for use with this material.

In order to get in production before the available quartz was used up, the development of methods of growing and of fabrication of the units was carried on simultaneously with the design and the working out of the manufacturing methods and the tools needed for quantity production. In some cases tools were designed and ready for use before the manufacturing process which required them had been proved satisfactory in the laboratory.

At the present time EDT crystals are being produced commercially. Many preproduction filters containing EDT units have been shipped to the field and are giving satisfactory service in different parts of the country. Commercial production has started on both EDT crystal units and the filters which contain them.

Owing to the success of this development program and to improvements in manufacturing yield in the use of quartz, all the crystal filters needed for extension of the long distance circuits will be produced.

The demand for large quartz crystals has been reduced to the point where the difficulty of a foreign source of supply should no longer be troublesome.

Precise Turbine Governor

HENRY E. WARREN
ASSOCIATE AIEE

AN IMPORTANT IMPROVEMENT in the art of regulating water or steam turbines has been made possible by the development, during the past two years, of a very precise governor.

The prime movers in all generating stations are provided with governors, nominally for the purpose of controlling the speed of the prime movers and consequently the frequency of the generators. These governors do in fact control speed until the generators are connected in parallel with other generators on a system. Thereafter they control primarily the load distribution among the machines.

Governors are almost all very similar in principle of operation and, generally speaking, in performance. Most of them are considered satisfactory because they behave well when controlling units on large systems. This apparently satisfactory service results because the function of these governors is then little more than to hold the gates of their turbines in substantially fixed positions, except when they are adjusted by hand or by auxiliary devices to reduce or increase the output of their units. They are always ready to respond to large speed changes but these large speed changes almost never occur except from accidental trouble. Actually the governors may be considered almost equivalent to safety devices which prevent runaway upon sudden loss of load.

Conventional Governors

The essential elements of a conventional governor are shown in Figure 1. This type of machine may be described briefly as follows:

A powerful hydraulic motor or "servomotor" attaches to the gates of the turbine prime-mover. This servomotor operates on the hydraulic energy of a liquid, stored in a tank under a pressure of several hundred pounds. It is very accurately controlled by a combination of valves which normally includes two units. One of these units is quite large and the other comparatively small and easily displaced by relatively slight force. As indicated in Figure 1 the smaller valve so controls the hydraulic energy supply as to make the larger one track all its own movements, but with vastly greater capacity and with negligible reaction on whatever mechanism initially operates the smaller valve. In effect, the two valves may be looked upon as one valve which combines ample capacity to operate the largest servomotor with insignificant opposition to its own displacement.

This resultant valve, whose operating point is that marked A in Figures 1 and 2, is itself under the control of four distinct elements. The first of these is a speed-sensitive device or tachometer, generally consisting of revolving weights free to move radially against the opposition of a spring; the second is the driven end of a dashpot whose driving end is connected to the gates of the turbine; the third is also a connection to the turbine gates, but without dashpot; the fourth is a member moved by a motor under control of the switchboard operator.

The utility of this construction may be explained most clearly by considering two typical cycles of operation. Suppose first that the generator attached to the turbine in question is not connected to any other generator. Then any decrease in its load leads to an increase in its speed, which in turn causes the tachometer stem to drop, and the valve to open in such direction as to close the turbine gates; these in closing act through the dashpot and to a much smaller extent through the other element operated by the gates to close the valve; the valve is thus closed before the tachometer itself returns to the position from which it started; this results in stable operation, but it also results in a change of that tachometer stem position which corresponds to the closed position of the valve; this change is such that now the speed is slightly higher than it was originally, although far less so than if there had been no governor. This is a manifestation of what is called "speed droop." Because of the dashpot, the effect of the gate motion on the valve can be large at the beginning of a governing cycle and thereafter decrease gradually and automatically as the cycle proceeds. Thus the ultimate change in speed because of a change in load would be nonexistent were the dashpot-equipped connection the only one between the gates and the valve operating point. This, however, is not permissible in the case of a unit which is to operate in parallel with other units, since isochronous alternators offer no means of controlling the division of load among themselves; hence the inclusion of a rigid supplementary connection from the gates back to the valves. Obviously, the effect of this connection can be undone by the motor driving the fourth element; this motor may be manually started and stopped, or may be made responsive to a variety of automatic devices.

In the second case, consider the generator connected with other generators on a large system, the steady state frequency of which is determined by some other governor or governors. There is only one position of the gates at which the governor valves remain closed, since the speed is fixed, fixing the tachometer stem position, and the gates are rigidly linked to the valves through the supplementary connection. The only means of changing the gate position is the motor of the fourth element. Thus, for an externally fixed system frequency, this motor becomes a

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HENRY E. WARREN is president of the Lombard Governor Corporation, Ashland, Mass.

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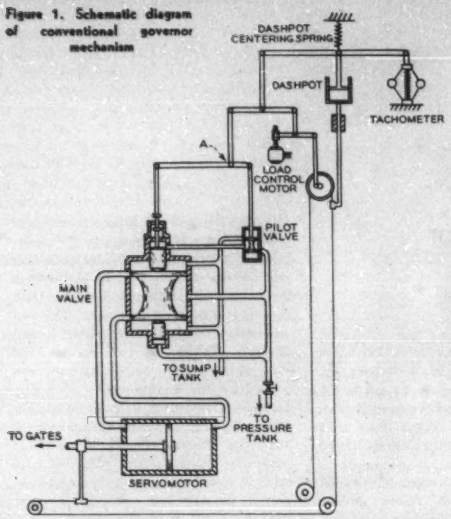


Figure 1. Schematic diagram of conventional governor mechanism

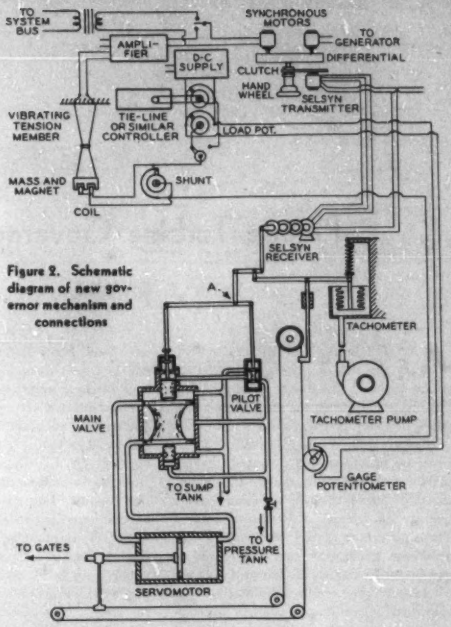


Figure 2. Schematic diagram of new governor mechanism and connections

load control device, and the entire governor is a load governor rather than a speed governor. Of course, should the generator become separated from the fixed-frequency system, the governor would instantly resume its function of speed control, and return the speed of its unit to whatever value the position of the "load control" motor at that instant called for.

The New Governor

The new governor mechanism is shown schematically in Figure 2; Figure 3 illustrates a demonstration governor of the new construction. Aside from the fact that a liquid-type tachometer is substituted for the fly-ball form shown in Figure 1, the principal difference between the new governor mechanism and that of the conventional governor is that the place of the dashpot is taken by a selsyn motor of a standard type. This forms the terminating element of an electromechanical device that performs the oil-dashpot function just described with greatly improved precision and reliability. In addition it replaces the load control motor and offers possibilities of accurate load and speed control not heretofore attainable directly.

Thus the dashpot with its necessary oil, which has always been troublesome, is completely eliminated. There remains in this new governor a simple speed measuring device, preferably a metal bellows opposed by a spring and supplied by liquid from a centrifugal pump directly driven by the turbine or by a synchronous motor. However, any standard form of governor tachometer having spring opposed centrifugal weights may be used equally well. The function of the tachometer, as in the conventional governor just described, is to operate the valves so as to maintain approximately constant speed on the unit.

The electromechanical system of which the selsyn receiver incorporated in the governor forms the last element is shown diagrammatically in the upper part of Figure 2. It consists of a pair of synchronous motors jointly operating a mechanical differential which in turn operates the transmitter end of the selsyn combination. A simple clutch and handwheel are provided so that the station operator may operate the turbine gates directly from his switchboard; this is a quick way of resetting the load on a unit, and, since the handwheel is easily provided with an approximate gate-opening scale, it is preferable to the tentative and unprecise scheme of push-buttons conventionally used to operate the "load-control" motor of Figure 1.

One of the two synchronous motors which operate the selsyn transmitter is permanently connected to the generator under control; the other may be connected either to the system bus, or to a suitable source of reference frequency. With the first mentioned connection, there is provided a means of exact adjustment of the speed to its desired value as measured by the system frequency. The unit, even when started from rest, will come up very quickly and stably to full and exact system speed. It will remain at this speed indefinitely, duplicating of course all minor changes in the system speed, but always ready to be connected to the system. Thus the problem of putting a new unit on the line is considerably facilitated. After the unit has been connected to the system by this method the two synchronous control motors will always run at exactly the same speed, therefore the turbine output will remain wherever it may be set. Under these conditions the unit will not tend to correct the system frequency.

If the supply to one of the synchronous control motors is now shifted to the reference frequency the unit will immediately try to correct the system frequency to correspond with that of the frequency

standard. If the system is in error the output of this unit will increase or decrease in such sense as to correct the error. It will be noted however, that all governors of this type, wherever they may be located on the same system, always act harmoniously; that is, each begins to respond to a minute frequency deviation at the same instant and in the same direction, since they are all measuring frequency against a high precision standard instead of being limited in this respect by mechanical features as are the present governors.

Consequently the use of governors of this type will tend to eliminate one of the difficulties now commonly encountered, the unco-ordinated absorption of load changes by machines whose governors behave erratically within and at the edges of their "dead bands." It is obvious from the foregoing that this governor cannot have a dead band, either as to speed or as to load, because it necessarily continues to act until the speed it is controlling matches the reference speed. The manner in which the equivalent as regards load comes about will appear in what follows. This is advantageous in that unnecessary changes in frequency and gate position are reduced. A differential selsyn could be used instead of a pair of synchronous motors with mechanical differential.

Problems of System Regulation

Modern power systems are so large that the problem of maintaining frequency as such no longer occupies a prominent place in practical system regulation. Of the load changes normally experienced nowadays, the more rapidly occurring ones are sufficiently small, relative to the stored energy in the system, and average sufficiently near zero, to cancel out and leave only a drift to be corrected. There could hardly be a form of frequency change better suited to correction by governors of this new kind. It is perhaps not unfair to say that the exact opposite is true for conventional governors, since these measure speed, not in terms of speed itself, but rather in terms of the position of a group of levers subject to friction errors. However, the same system expansion which minimized the immediate frequency problem has introduced others; for example, it is no longer feasible to entrust the final supplementary regulation to one station whose governors are equipped with master clocks; neither tie-line capacity nor reserve requirements could permit this procedure with a large modern system.

The solution to date has been the addition of more and more complicated supplementary devices to the governing apparatus; the overwhelming majority of them operating through the load control motor of the fundamental governor. When the delays and irregularities inherent in this method are considered, the extent to which it functions satisfactorily seems remarkable.

Figure 4 illustrates the behavior of a large modern system as regards frequency. The chart was made by means of a synchronous motor-differential recorder very similar to the frequency comparing mechanism shown in Figure 2; this yielded the integral of the frequency deviation, curve *A*. This curve was differentiated graphically to obtain the frequency, curve *B*. From the fact that the frequency changes but slowly, and shows no marked short period irregularities, it seems clear that the major burden of system regulation is borne by the precise supplementary regulators.

The first supplementary devices applied to governors were master clocks; much of the final regulation imposed today remains based on clocks of one kind or another. It is of course true that time keeping is one of the services supplied by power companies, and that the income they derive from it is far from negligible. This does not mean that all regulation must be subordinated to time service, and the more recent regulating systems are largely based on actual frequency for all short period operations, and return to timekeeping only over much longer intervals. From the viewpoint of actual system operation, the principal virtue of accurate frequency regulation is that it facilitates proper tie-line loading and economic load distribution. This does not change the fact that the most sensitive measure of frequency, with simple apparatus not requiring elaborate energy amplification, shares with timekeeping the quality of being a measure of integrated frequency. This is the sort of apparatus used in this governor, but here the interval over which the integrating process is conducted before the resulting measure of frequency is applied to the governor is reduced to zero.

Furthermore, the short period reference frequency used is not necessarily the frequency of the earth rotating about its axis, as it is in timekeeping, but is instead an approximation to it readily modified by such things as tie-line wattmeters or other devices, Figure 2. It is possible, by means of this direct, powerful, and rapidly acting measuring device forming a part of the governor itself, to eliminate the complicated conventional combination of an insensitive, imprecise, and somewhat unpredictable governor with a sensitive, but complex and indifferently well articulated supplementary regulator.

Figure 3. A demonstration governor of the new type

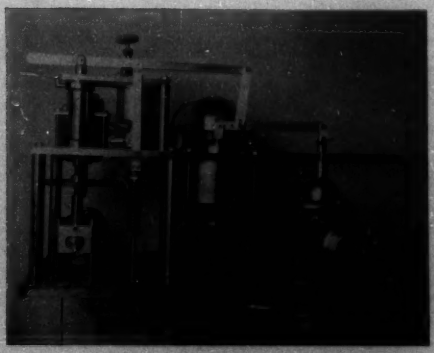
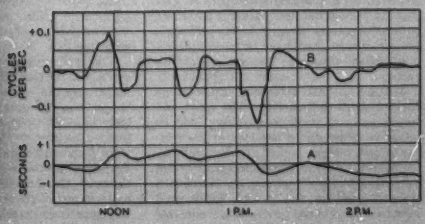


Figure 4. Frequency deviation on a large system and its integral



The Reference Frequency

The source of reference frequency employed in the governor is a modified form of the vibrating wire standard used for telescope control on Mount Palomar. This latter device was described in a paper published in 1939.[1] In such instruments the rate of vibration of a tension member supporting a relatively heavy mass is determined sharply by the total force exerted by the mass. Since a portion of this mass is a large circular Alnico magnet having a coil of wire located in its air gap, the gravity force can be increased or diminished by an attraction or repulsion force between the magnet and a small current passing through the coil. The amount of this current is accurately controlled by the potentiometers shown in Figure 2 at several points.

One of these potentiometers, being operated mechanically by the gates of the turbine, tends to decrease the reference frequency as the gate opening increases. This is equivalent to the conventional speed droop, and its magnitude is readily controlled by the setting of the shunt across the coil. The manually operated potentiometer at the switchboard determines the gate opening at which the reference frequency matches the system frequency when the latter is normal; this means that the potentiometer sets the normal load carried by the unit. The unit's deviation from this normal load constitutes its contribution to the regulation of the system, and can be made either large or small as desired by resetting the shunt.

The operator also has, through the simple friction clutch and handwheel described before, direct control over the selsyn motor which takes the place of the dashpot. Therefore he can almost instantly open or close the water wheel gates to any desired extent in a very definite way. As before mentioned an indicator on the selsyn generator at the switchboard shows on a scale the approximate gate position at all times.

Conclusion and Summary

The very complicated problem of load and frequency control over great systems will obviously not be solved completely by the substitution of this kind of governor. Economic generating requirements, reserve power needs, and tie-line loading must have human attention. It is the writer's opinion that operating difficulties will be lessened in proportion as the governors are made more sensitive to the frequency, so that they may all act consistently.

It seems probable that if enough units on a large system could respond immediately to extremely small deviations from exact standard frequency, the change in power output for each unit would be so small as to be almost negligible and the load dispatchers would need only to add and subtract blocks of power from time to time to conform with the relatively predictable large load variations. There would of course remain the problem of tie-line interchange between large areas. This would require, as at present, governor biasing on certain units, which can be done with a special ease and precision on the new form of governor. The new governor does not involve any radical changes in the general method of moving the gates or valves of a turbine. There is much to be said in its favor on account of the elimination of any dashpot. The cost of installation should compare favorably with that of the present type of machines, and should be appreciably less where governors must be equipped with precision auxiliaries.

Summary of Methods and Results Characteristic of New Governor

1. Speed measured approximately by a conventional type of tachometer, with ample speed droop for stability.

2. Precise speed adjustment superposed on approximate speed adjustment by comparing frequency of unit with a reference frequency. This comparison is made by a differential coupling between two synchronous motors or by a differential selsyn. In this manner the need for a dashpot is eliminated with benefit to stability and precision.

3. Provision for direct tie-line load and other control incorporated in governor.

4. Elimination of dead band of governor, and uniform behavior of all governors of this kind on a system.

5. Governor control and frequency comparison instruments located at switchboard. Operator can make definite adjustment of load.

6. Complete control at switchboard of the governor's contribution to system regulation.

7. Reduction of time required to bring unit from rest to exact system speed.

Reference

1. A New Time Standard, **Henry E. Warren.** AIEE Transactions, volume 59, 1940, pages 137–41.

Discussion

W. M. Pickslay (Pacific Gas and Electric Company, San Francisco, Cal.): From the system aspect, it would appear that the performance of this governor is exactly the same as that of the present more conventional governor with one important exception. That is, both governors are characterized by a drooping speed-load or gate opening curve, but the author's governor, at least for small frequency deviations, is sensitive to the integrated frequency deviation rather than the instantaneous frequency.

Because of this difference, there is no direct means of measuring the exact improvement in preciseness that can be realized. However, a fair idea of the relative improvement would be secured by comparison of the exact frequency and integrated frequency deviations which will cause motion of the gates. In a good modern conventional governor, a frequency deviation of 0.006 cycle (2.16 degrees per second) may be expected to initiate corrective action. While Warren speaks of elimination of "dead band," it is from the point of view of the new governor's continuing to act until there is an exact match between controlled and reference frequencies. Owing to friction load on the selsyns together with their normal positioning error of 1 to 2 degrees, there must be some finite value of integrated frequency deviation required to take place before gate motion will result. The magnitude of this is required for the comparison.

The improvement realized must be evaluated further from another point of view. During system frequency changes, the sooner the gates are moved, particularly on units nearest the load change, the less will be the excess gate opening or kilowatt generation required to prevent further deviation and to restore the frequency to normal. On the other hand this results in more frequent governing. Experience will be required to determine whether or not it will result in excessive maintenance of governor, valves, and gate mechanisms.

The author assumes that frequency deviations occur simultaneously throughout the system. This is implicit in the statement to the effect that all the new type governors begin to respond at the same instant. Actually, a frequency deviation due to a load change at one point in a system will be some time in showing up at a remoter point since an angular difference between ends of transmission links must appear before the transmitted power can be altered to produce a change in frequency at the farther end.

While increased sensitivity may be expected to reduce the magnitude of tie line power swings, supplementary control is still necessary for steady state control of power interchange beyond that afforded by droop settings. If, however, the reference frequencies of all units so governed were synchronized with zero droop, it is probable that supplementary control could be eliminated and increased maintenance justified. An approach to this has been previously

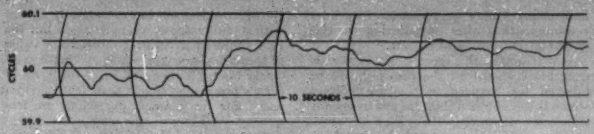


Figure 1

proposed[1] which not only should result in improved control of individual and interchange power but also contribute directly to system stability during fault or emergency conditions.

REFERENCE

1. POWER SYSTEM GOVERNING, T. E. Curtis. ELECTRICAL ENGINEERING, December 1947, pages 1197–1200.

J. E. Allen (Pennsylvania Water and Power Co., Baltimore, Md.) and **W. B. Hess** (Safe Harbor Water Power Corp., Safe Harbor, Pa.): The author is to be commended for his pioneering efforts to find a new solution to the problem of turbine governing. The system which he describes has several novel features such as the frequency standard with means for introducing speed droop by shunting coils, the differential method of comparing the turbine speed with a reference standard, and elimination of the dashpot.

It is noted that the author refers to his governor as being a precise one and that it has no dead band. However, no performance data have been given to show whether or not this characteristic has been realized in practice. If there is no dead band it would be expected that a card diagram of frequency versus gate opening with the turbine governor regulating would be a line with slope corresponding to the inherent speed droop. This ideal has never been fully realized in existing governors but excellent performance approaching this ideal, especially for steam governors, has been disclosed in publications.[1,2] In the discussion to reference 1, and in reference 2, test records are shown of very sensitive steam governors having card diagrams resembling straight lines. Under the same discussion an instance is also shown of a governor for a large hydraulic turbine which is considered excellent for a governor having a compensating dashpot. Progress made in instrumentation now permits the obtaining of card diagrams of frequency versus gate opening while the governor is regulating on the system. It would be desirable, therefore, to present such performance records of the precise turbine governor to show how much the art has progressed. It would be especially interesting to see the record of performance of this governor when applied to a hydro unit.

It is not clear when the author refers to the governor having no dead band whether it is meant to include the pilot and main valves. In accordance with Joint AIEE-ASME* Committee on Recommended Specification for Prime Mover Speed Governing, dead band refers to the relationship of the turbine operating piston with respect to a given speed change. Pilot and relay valves usually have a certain amount of lap which would introduce some dead band. It would be helpful if the author would clarify his definition of dead band.

The speed droop is obtained by shunting the coils of a frequency standard energized from a d-c supply. It is obvious that the source of d-c would have to be stable; otherwise, it would affect the governor performance. It would be interesting to know just what degree of stability is required for the d-c voltage supply.

The author comments on the deficiencies of existing governors. In some instances his comments are well founded. However, improved performance can frequently be obtained by proper adjustments and adequate maintenance. Instruments are necessary to accurately and readily measure the performance characteristics and to evaluate the results of corrective measures.

REFERENCES

1. TESTING GOVERNOR PERFORMANCE ON ELECTRIC POWER SYSTEMS WITH IMPROVED INSTRUMENTS, J. E. Allen, W. B. Hess. AIEE TRANSACTIONS, volume 66, 1947, pages 329–39.

2. A TURBINE-GOVERNOR PERFORMANCE ANALYZER, Discussion, R. Sheppard, C. Concordia. AIEE TRANSACTIONS, volume 61, 1942, pages 393–4.

C. L. Avery (Woodward Governor Co., Rockford, Ill.): Figure 1 is incomplete in that a by-pass needle valve should be shown in the dashpot. The operation of a conventional governor is then more correctly described:

"Because of the dashpot by-pass, the effect of the gate motion on the valve can be large at the beginning of a governing cycle and thereafter decrease gradually and automatically as the cycle proceeds."

Functionally the system shown in Figure 2 continuously corrects the speed level setting of the governor mechanism through a speed matcher. Continuous correction appears to be an improvement over the impulse corrections in common use. There is nothing new and different about speed matching. The speed matcher is an established device. Speed matchers of a more direct acting type have been used for over twenty years. The novelty of this system lies in the control system which actuates the speed matcher. If this system has any practicable advantage over the supplementary regulators presently available, it can be applied to existing governors without difficulty.

With reference to hydraulic turbine applications, when the dashpot is eliminated, some other means of stabilizing the unit must be provided. This is accomplished in this type of control by imposing permanent speed droop on the "tachometer." For medium head wheels with fairly long penstocks, stabilized by droop alone, values in the neighborhood of twenty per cent or higher are required. It would be interesting to know whether this governor has been applied to turbines of this type.

Presently available modern governors respond to very small speed changes. (Smaller, in fact, than can be detected by commercially available frequency meters.) They respond and continuously correct the loading on the controlled units as dictated by the speed droops to which they are adjusted. For the sake of simplicity of explanation, it is a common fallacy in discussing governors to assume that a large system operates at constant frequency. No generating system ever operates at a constant frequency for long. Therefore it is important for a governor operating on a large system to follow the frequency, and accordingly correct the load on its connected unit as faithfully as possible in order not to be entirely out of step with the instantaneous demand of the system. The time lag between the cause and effect should be held to a minimum.

Figure 4 shows time lags in the order of ten minutes between the frequency line *B*, which is the indication of load demand and the integral of the frequency deviation line *A*, which is the indication of load requirement as received by the governor mechanism shown in Figure 2. Furthermore, Figure 4, which shows the trend of average frequency, does not give a typical picture of the instantaneous frequency variation on a power system. Figure 1 of the discussion for instance, taken on a large system, using a high speed, highly sensitive frequency meter shows that frequency actually fluctuates much more violently than would be expected from Figure 4.

Lacking performance data, it is not possible to discuss the performance of the control system described by Warren. An analysis of such data when it is made available should not disregard the above mentioned requirement for good control on central systems.

Henry E. Warren: In answer to the discussion by Allen and Hess, I would say that the reason why the ordinary kind of turbine governor has a dead band while the new Warren precise governor does not have a dead band is as follows:

In the conventional type of governor the entire sequence of motion depends primarily upon the measurement of speed by balancing the centrifugal force developed by revolving weights against a spring. This balancing is obtained by the use of parts which include bearings, pivots, and collars. All of these have slight friction. Since there is friction the equilibrium position for any given speed is slightly indefinite and this indefiniteness is responsible for part of the so called dead band. In addition, however, there is usually a slight amount of lap either negative or positive in the valve connected with the speed measuring element and there may be some end play in the connecting parts.

The sum of all these factors accounts for the dead band which may be considered as the minimum amount of speed change required for a positive movement in one direction or the other of the servo-motor piston. This definition corresponds with that of the recommended specifications of the ASME and AIEE. This is a static phenomenon and the failure of any response

* American Society of Mechanical Engineers.

to a slight error in speed may continue indefinitely. Such a dead band as this probably can never be entirely eliminated although it may be made very small.

The Warren governor does not depend upon a device which attempts to measure the exact value of the speed or frequency directly but in place of that there is comparison mechanism that determines the difference in motion between two revolving elements. One of these elements represents a standard speed or frequency and the other one the speed or frequency of the unit under control. Since both of these revolving elements are in constant motion and friction is not involved, there cannot be the slightest difference in speed without producing a differential resultant motion. This resultant motion is bound to continue as long as there is any difference in speed. Since this resultant motion brings about displacement of a valve and the movement of a servo-motor piston, even though the valve has lap it must move until the servo-motor has been forced to bring the frequency of the controlled unit to correspond with the standard frequency. The valve cannot rest until that result has been accomplished and therefore there is no dead band.

The action is dynamic rather than static as in the case of the present governors. The smallest measurable difference in frequency between the frequency standard and the controlled turbine generator must bring about speed adjustments of the turbine. The time required to do this depends upon the gear ratio of the differential frequency measuring device which takes the place of the dashpot in this governor. The action may be slow in the case of turbines which have long water columns or fast in the case of those that are situated in open water. The correction for a speed error is along an exponential curve similar to the discharge time curve of a condenser. This correction curve need not involve over-travel which is a common characteristic of standard governors. Frequency diagrams with varying load changes may be presented in a later communication after installation of this governor in a new power plant.

It is true that the speed droop in the arrangement shown in the drawing will be affected by changes in the d-c voltage which is the source of bias for the frequency standard. However, the effect of small variations in voltage of the d-c supply is very slight. For a bias of plus or minus 2 per cent a voltage variation of as much as 10 per cent would only affect the gate position of the turbine 1/5 of one per cent and such a variation in voltage would have no effect at all if the unit were running at standard frequency for system control.

In answer to the discussion by Pickslay, I would suggest that any ordinary conventional governor would need to be in exceptionally good condition to respond to a frequency deviation as small as 1/100 of a cycle from the value at which it was adjusted, and there would be a rather small chance that this adjusted value would be exactly 60 cycles. Looseness in any of the connecting parts or valve lap might ordinarily be expected to introduce a still greater error. This means that on any system no two governors would be trying to do the same thing at the same time.

It is true that in the Warren governor integrated frequency differences between a standard value and that of a controlled unit are responsible for initiating action, and this integration of the frequency error, which can take place very rapidly, accounts for the elimination of the dead band. In this form of governor an error as small as 1/10 of a cycle of the controlled unit can produce pilot valve displacement at the rate of 0.002 inch in 1 second but the rate may be increased if desirable. This amount of frequency deviation would require 0.1 second to overcome such a positioning error of the selsyn as 2 degrees which Pickslay suggests.

Of course it is true that a frequency deviation due to a load change at one point on the system does not instantaneously appear with its full value at a remote point but the time required to return to a uniform value of frequency over the whole system is relatively brief. This would be of the order of a very few cycles, or less than 1/10 of a second. For very small variations in frequency, time lags of this magnitude in correcting gate positions of turbines are probably of far less importance than the precision with which such corrections are made.

In reply to Avery, I would say that the refinement of a needle valve was omitted in Figure 1 of the paper because it was felt that it would be generally understood that a by-pass whether through a valve or elsewhere, is an inherent part of any dashpot; the equivalent refinement of a change in the connection ratio of the comparison device to the valve was also left out of Figure 2. As regards the matter of gradual decrease of the effect of the gate motion, such a decrease is automatically obtained in the mechanism connecting the gates with the valve in the Warren governor. The governor has indeed been operated against speed droops ranging up to 25 or 30 per cent. It is true that time lags between frequency changes and governor response must be minimized; it is also true that the base for corrective response must be accurate. Figure 4 is illustrative of relatively extended interval system conditions, and is not in any sense indicative of the rapidity of action of the Warren governor. In fact, as stated in the paper, the frequency curve shown in Figure 4 was graphically derived from the recorded integrated error curve, and any time lags shown there could only be the result of a lack of precision in the graphical differentiation. There is no theoretical lower limit to the minimum lag; the governor can be designed to have as small a lag as the application requires and conditions of gate activity permit. This governor aims to provide a supremely accurate base for response, with the response as speedy as conditions external to the governor itself permit.

A 150-Kc Carrier System for Radio Relay Applications

J. E. BOUGHTWOOD
ASSOCIATE AIEE

Synopsis: The carrier system described was developed by the Western Union Telegraph Company for use with microwave radio relay circuits. Thirty-two voice-band communication channels, each 3,000 cycles wide, are derived in the frequency range 4 to 147 kc. Tandem stages of group modulators maintain a high degree of spectrum efficiency and eliminate the necessity for crystal filters. A crystal-controlled harmonic carrier supply insures the frequency stability required by narrow-band frequency-modulation telegraph circuits.

THE telegraph company has made extensive use of carrier current systems to increase the message capacity of its wire plant and currently has more than 500,000 telegraph channel miles in operation.[1] These systems have greatly improved the efficiency of traffic handling and have an excellent record for circuit continuity. Nevertheless they are still subject, at times, to interruptions from extraneous electrical disturbances and from physical damage to wire lines.

Rapid wartime advances in microwave radio techniques created new tools for the communications engineer whereby a transmission medium of tremendous intelligence-carrying capacity, and yet economically attractive, could be realized. In co-operation with the Radio Corporation of America, an experimental radio relay system operating in the neighborhood of 4,000 megacycles was developed and placed in operation between New York and Philadelphia with repeaters at Woodbridge, New Brunswick, and Bordentown, N. J.[2,3] This circuit, under constant observation since early in 1946, has demonstrated that improved continuity of operation as compared with conventional open wire lines is attainable, subject only to equipment failures at terminals or repeaters. As such failures are amenable to design and maintenance procedures, it appears that nearly the ultimate in reliability can be achieved by the use of microwave radio systems.

The radio relay systems currently being installed to link New York, N. Y., Washington, D. C., and Pittsburgh, Pa., have useful transmission bands extending from 60 cycles to 150 kc. To integrate such systems into a telegraph carrier plant standardized on a basis of telegraph channel groups designed to occupy fully voice bands approximately 3,000 cycles wide, it was necessary to subdivide the 150-kc frequency bands accordingly. Both time division and frequency division methods for accomplishing this result were considered. The somewhat uncertain status of pulse systems as to flexibility and efficiency, where maximum utilization of a restricted frequency spectrum is a major consideration, dictated the use of a single-sideband frequency-division system as the most practical method for deriving the required voice bands.

General Description

The new carrier system, designated as type *WN*, furnishes 32 independent voice circuits and occupies the frequency range between 3.9 and 147.3 kc. Each voice band provides a useful transmission path extending from 300 to 3,300 cycles, sufficient to accommodate 18 narrow-band (150-cycle spacing) telegraph carrier channels[4] or 10 wide-band (300-cycle spacing) channels. Table I illustrates the message handling capacity of the system for various modes of operation. Where the 4-channel multiplex method is employed, an excellent frequency spectrum efficiency is realized requiring but 1.7 cycles of spectrum per word minute. Although this tremendous word capacity is quite practical of attainment, it is expected that the teleprinter method will predominate, the reduction in maximum capacity being more than compensated for by other operational advantages thereby achieved.

In its fundamental concepts, the *WN* system is similar to earlier wire systems developed by The Western Union Telegraph Company. By means of tandem group modulators and filters, the load capacity of the system is increased in geometric progression with each succeeding stage of modulation. The advantages of this method are that a minimum number of translation frequencies are involved and grouping filters can utilize coils of nominal efficiencies rather than the crystal units which would be required if each voice band were raised to its assigned "line" frequency by independent modulator stages. Crosstalk and distortion due to nonlinearities in amplifiers have been reduced to negligible proportions by the use of negative feedback and conservative load ratings. Modulators are of an improved balanced bridge type capable of operating at high signal-to-carrier

Table I

Type of Operation	Type of Channel	No. of Telegraph Circuits	Theoretical Capacity in Words Per Min
Teleprinter	Narrow band	576	34,600
2-channel multiplex	Narrow band	1,152	76,000
4-channel multiplex	Wide band	1,280	84,500

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J. E. Boughtwood is assistant to transmission research engineer, engineering department, Western Union Telegraph Company, New York, N. Y.

The author gratefully acknowledges the advice and counsel accorded him in the preparation of this paper by the several engineers concerned in the development of the *WN* system.

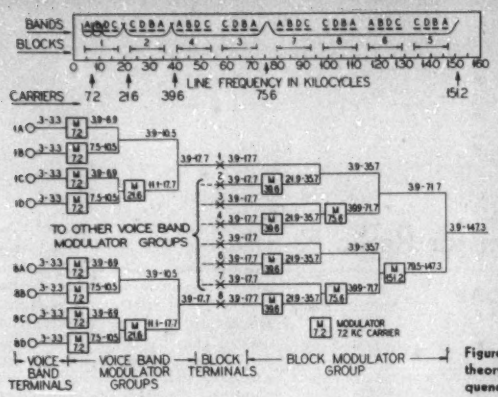


Figure 1. System theory and frequency allocations

ratios without appreciable distortion. The required carrier frequencies are derived from a single base frequency oscillator and harmonic generator. A temperature-controlled crystal oscillator and associated electronic regulator circuit stabilizes the base oscillator to the desired accuracy.

A simplified system theory diagram is shown in Figure 1 with the corresponding "line" frequency allocation of each voice-band indicated. The pattern of geometric progression is quite evident in the band-frequency chart, and is employed in all but the initial modulation stage. The minimum line frequency of 3.9 kc simplifies to some extent the design requirements of various components and also eases the transmission problems encountered where the radio and carrier system equipment are remote from one another.

Table II has been prepared to illustrate the path followed by a given voice band in reaching its assigned line frequency. It shows the frequency translations encountered by a 1,000-cycle tone applied to voice band *8-C* before reaching its ultimate position at 99.8 kc. Five translations are involved and, conversely, a similar number must be traversed at the receiving terminal before the 1,000-cycle tone again appears at the output of voice band *8-C*. Voice band *8-D* also is subject to five translations, but in this case the upper side band of the 7.2 stage is selected and the resulting line frequency is 101.8 kc. Bands *1-A* and *1-B* are translated but once at each terminal. The remaining bands, of course, fall between these two extremes.

In a communication network comprising a large number of radio relay systems, certain groups of voice bands may encounter several such systems in tandem between point of origin and destination. Traffic dispatching can be expedited and equipment economy realized if these bands are grouped into blocks and each block handled as a single unit. This arrangement has been incorporated in the system by providing the patching jacks indicated by *X* on Figure 1. A 4 voice-band grouping point was selected as the most practical, although the 2-, 8-, or 16-band points could as readily be made available if desired. Greatly increased system flexibility is achieved as the equipment to the left of the indicated patch points can be moved from one system to another as traffic or emergency conditions dictate. The arrangement also permits voice band blocks to be cut through intermediate terminals without demodulating each band to voice frequency. As a result, considerable economy in equipment is realized since that portion of the system capacity reserved for spare or cut-through service need not be equipped beyond the block patching point.

The system thus divides itself into two types of modulating equipment; one designated as a voice band modulator group to combine four separate voice bands into a single block, and the other, a block modulator group, to combine eight such blocks for transmission over the radio relay circuit.

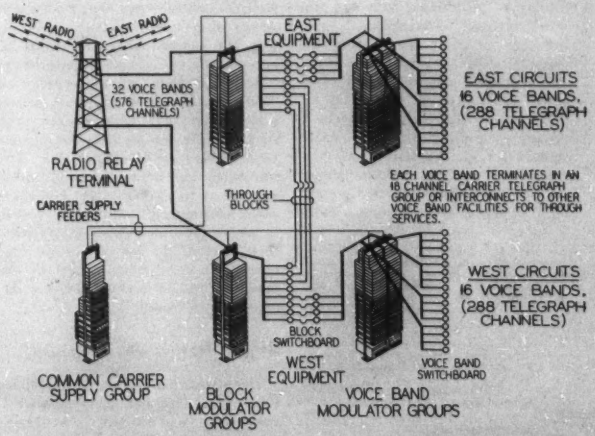
The general physical appearance of a typical installation involving two *WN* system terminals with a common carrier supply group is shown in Figure 2.

System Details

Voice Band Modulator Group

A voice band modulator group, employed to handle the four individual voice bands which make up a block, is shown schematically in Figure 3. The number of voice band modulator groups associated with a given *WN* system depends upon traffic requirements. Not more than four groups, occupying two repeater racks and furnishing a total of 16 voice bands, would be normally assigned, leaving half the system capacity

Figure 2. Typical intermediate station involving two radio relay terminals



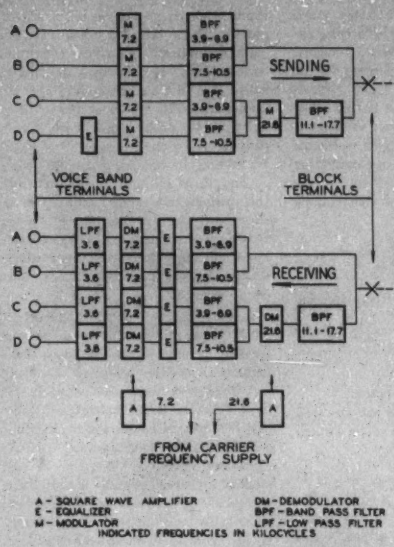


Figure 3 (left). Voice band modulator group

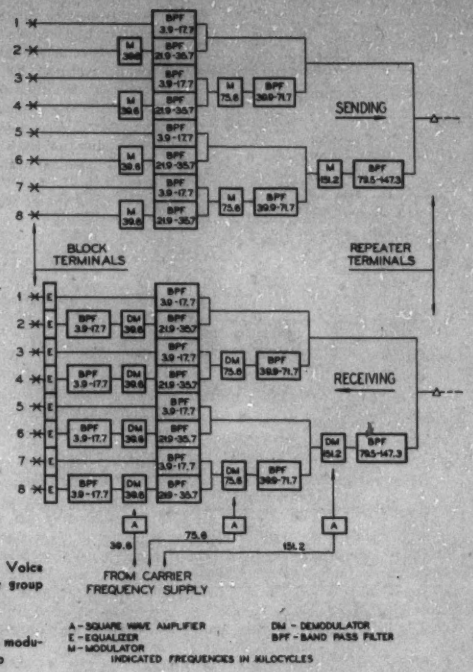


Figure 4. Block modulator group

available for spare or cut-through service.

The function of the sending portion of the modulator group is to accept four identical voice bands and arrange them one above the other in the frequency range 3.9–17.7 kc. Each voice band is initially modulated by a 7.2-kc carrier, the lower side bands of *A* and *C* and the upper side bands of *B* and *D* are selected separately by appropriate filters. Since *A* and *C* now both lie in the frequency range 3.9–6.9 kc, and similarly *B* and *D* occupy the 7.5–10.5 kc-region, bands *C* and *D* are combined, and again modulated by a 21.6-kc carrier and the lower side band 11.1–17.7 kc selected. Each voice band now occupies a different part of the spectrum so that all may exist simultaneously at the output.

The receiving portion of the modulator group functions in the converse manner to restore the voice bands to their original frequency location.

Block Modulator Group

The 150-kc transmission band furnished by the radio relay circuit is channelized into eight 3.9 to 17.7-kc blocks by the block modulator group shown schematically in Figure 4. Each block will accommodate one voice band modulator group giving a maximum system capacity of 32 voice bands.

In the "stacking" of the individual blocks, the lower side band of the modulation process is utilized in all cases. At the sending terminal block 1 encounters no modulator and reaches the line in its original frequency location. Block 2 is raised to the frequency range 21.9 to 35.7 kc by a 39.6-kc modulator and paralleled with block 1. Blocks 3 and 4 are combined similarly and then translated to the frequency range 39.9–71.7 kc by an additional 75.6-kc modulator before joining blocks 1 and 2. Blocks 5 to 8 are treated identically and raised to the frequency range 79.5 to 147.3 kc by a final 151.2-kc modulator before reaching the line. Each block now occupies a different portion of the 3.9 to 147.3-kc spectrum for transmission.

The receiving portion of the block modulator group utilizes a similar method in the reverse order to restore each block to its original frequency location.

Modulator Circuits

Much has been published in connection with modulators,[8] particularly the double-balanced ring type commonly used in single-side-band carrier-suppressed systems, shown in (*a*), Figure 5. In idealized theory, this modulator is treated as a double-pole double-throw reversing switch which operates back and forth at the carrier frequency and whose reversing time is zero. Under this condition, distortion products involving harmonics of the signal are nonexistent. In practice small rectifier elements perform the switching function under control of the applied carrier voltage. For the polarities shown, elements *A-A* are polarized in the conducting direction and offer a low impedance to the signal currents, while elements *B-B* are polarized in the nonconducting direction and offer a high impedance to the signal. The output current flows as indicated. When the carrier voltage reverses, the im-

Table II

Input Frequency, Kc	Translation Frequency, Kc	Output Frequency, Kc
1.0	7.2	6.2
6.2	21.6	15.4
15.4	39.6	24.2
24.2	75.6	51.4
51.4	151.2	99.8

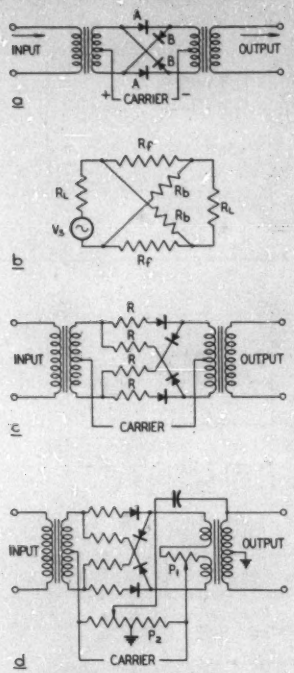


Figure 5. Modulator circuits

pedance conditions of the rectifiers reverse as does the output signal current. If the rectifier impedances are independent of the signal currents flowing through them, then the equivalent of true switching action can be achieved. However, with a sine wave carrier, there is a definite proportion of time at the crossover when the signaling current is equal to or greater than the carrier and during this interval severe distortion can occur. For this reason, the ratio between carrier and signal powers must be large if distortion in wide-band group modulators carrying a multitude of separate signal frequencies is to be maintained at an acceptable level. A logical solution is to employ a square wave carrier voltage which switches the rectifier elements abruptly between the conducting and the non-conducting conditions and reduces to negligible proportions the time interval during which the signal can influence the rectifier impedances. A reduction in distortion components of at least 25 decibels can be achieved easily by this method. Another factor which may seriously limit the load carrying capacity of the double-balanced modulator can be explained by reference to the equivalent circuit shown at (*b*). Idealized rectifiers are assumed having a low resistance R_f when polarized in the conducting direction and a higher resistance R_b when polarized in the nonconducting sense.

The signal voltage across a conducting rectifier is

$$E_s = V_s \frac{R_f}{2(R_L + R_f)}$$

The carrier voltage across a conducting rectifier is

$$E_C = \frac{I_C}{2} R_f$$

where I_C is the total carrier current.

For a square wave carrier and idealized elements, overload will be reached when $E_s = E_C$ or

$$V_s(\max) = I_C(R_L + R_f) \qquad (1)$$

Similarly for a nonconducting element

$$E_s = \frac{V_s}{2} \text{ approximately}$$

$$E_C = \frac{I_C}{2} R_f \text{ as before}$$

Overload again occurs when $E_s = E_C$ or

$$V_s(\max) = I_C R_f \qquad (2)$$

Obviously equation 2 is smaller than equation 1 and is the first to be exceeded as the signal input is increased. In a typical case where $R_L = 600$ ohms and R_f about 50 ohms, the nonconducting rectifiers will overload with a signal less than one-tenth that required to overload the conducting branches.

This condition is remedied easily by increasing the carrier voltage across the nonconducting elements. Diagram (*c*) shows one of several possible methods for accomplishing this result, in this case by adding resistors R in series with each rectifier element. Although this method, in contrast to other possible methods, introduces some loss in the signal path, it is preferred because of its simplicity and excellent balancing properties.*

Germanium rectifier elements with either welded or pressure contacts replace the copper-oxide type used with previous carrier systems. They are more stable with respect to temperature and mechanical shock so that modulators employing these elements have excellent balance stability. This is an important consideration in sending modulators where the desired sideband and the carrier frequency are in such close proximity as to prohibit adequate carrier suppression by the grouping filters alone. By adding the balancing potentiometers P_1 and P_2 as indicated in diagram (*d*), the fundamental carrier output can be suppressed to any desired degree. In the receiving demodulators the desired side band and the carrier are always widely separated so that a precise carrier balance is not necessary.

A power level of plus 10 decibels referred to one milliwatt at the voice-band patching point is the standard level employed by the telegraph company for all carrier systems. To maintain this level throughout the modulator stages of a *WN* system would require electronic components of large power carrying capacity. Therefore, the 7.2-kc sending modulators are designed for a single side-band loss of 30 decibels so that voice-band loads entering the system at plus 10 are reduced to minus 20, this level being maintained throughout the sending terminal by single-tube low-power amplifiers associated with each modulator stage and having sufficient gain to compensate for filter losses.

The receiving terminal operates at a somewhat higher level to insure an adequate margin of adjustment when block patching between different systems. For this reason and also because distortion components in the demodulator amplifiers, particularly those involving harmonics and difference products of the signal frequencies are so located as to produce crosstalk between bands, 2-tube amplifiers of higher power capacity and lower distortion than the corresponding units in the sending terminal are employed. The final 7.2-kc receiving demodulator has sufficient gain and power output to restore the individual voice bands to the standard plus 10 decibels referred to one milliwatt level.

To facilitate system adjustment and

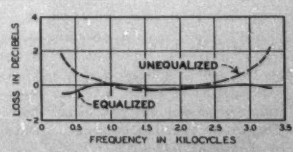


Figure 6. Voice-band transmission frequency characteristic

* The advantages of a square wave carrier and the self-biasing technique in the performance of the balanced modulator, were covered in a paper (unpublished) by F. H. Cusack presented before the AIEE Philadelphia (Pa.) Section, March 25, 1946.

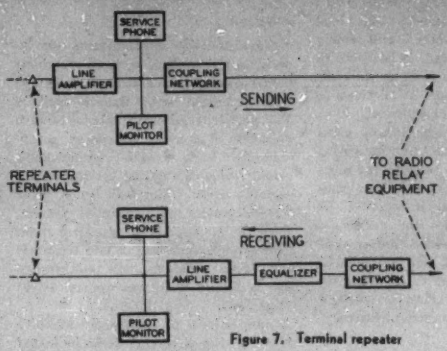


Figure 7. Terminal repeater

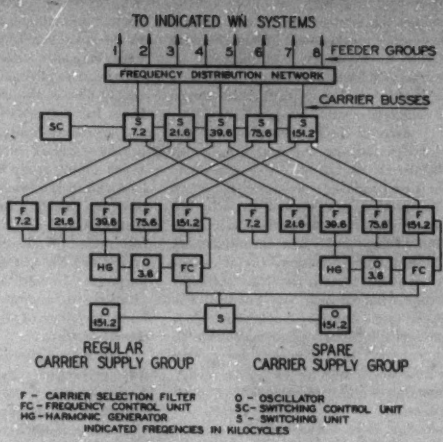


Figure 8 (right). Carrier supply group

maintenance procedures a high-frequency 600-ohm decibel meter was developed. The meter is a rectifier type similar in principle to the current instrument used with lower frequency wire line carrier systems.[6] Germanium crystal rectifier units and a shielded high-frequency adjustable attenuator network are employed to obtain a flat frequency response. The response at 1,000 kc is down only 0.3 decibel. Levels between minus 15 and plus 35 decibels referred to one milliwatt can be measured with an accuracy of 0.2 decibel in the 150-kc range.

Filters

The excellence of a carrier system depends to a large extent upon the quality of its filters. The design of the *WN* system filters, covered in a current paper,[7] are predicated on a minimum signal-to-crosstalk ratio of 50 decibels for each voice band, a quite adequate margin for carrier telegraph service and sufficiently great to allow many such systems to operate in tandem. The end impedances are 600 ohms and sufficiently well balanced with respect to ground to mitigate the effects of longitudinal voltages which may be induced in external wiring. Both block and voice-band patching points are equalized where necessary to maintain a substantially flat frequency characteristic. The transmission-frequency characteristic of a typical voice band, showing the effect of the equalizing network, is given in Figure 6.

Terminal Repeater

The function of the terminal repeater, Figure 7, is to bridge the gap between the channelizing equipment and radio relay terminal and to provide necessary monitoring and talking facilities. A pilot channel operating at 19.8 kc gives a continuous check of the circuit transmission equivalent and also may serve as a Morse or teleprinter talk circuit. A service phone channel occupies the unused portion of the spectrum below 3 kc. The line coupling panels match the 600-ohm terminal impedance to that of the particular circuit connecting to the radio terminal. Where the circuit is of appreciable length, receiving equalizers maintain a flat frequency characteristic.

Carrier Supply Group

A measure of the frequency stability of a carrier system is the maximum amount by which voice frequencies, after passing through the system, differ from the frequencies originally transmitted. Frequency modulated telegraph channels will be subject to bias losses if this difference is excessive. The possibility of such losses is reduced to negligible proportions in the *WN* system by designing the carrier supply group for a frequency stability of better than 0.00066 per cent.

It may be of interest to illustrate the relation between a given frequency error in the system base oscillator and the resultant error in the line frequency assignments of the various voice bands. In a carrier system employing tandem stages of modulation where the lower side band in each stage is utilized, the line frequency error is

$$\Delta L = \Delta C(n_1 - n_2 + n_3 - n_4 \ldots \text{etc.})$$

where ΔC is the base frequency error in cycles and n_1, n_2, etc., are the harmonic relation of the carrier frequencies to the system base frequency, listed in the order used. For example the line frequency error in voice band *8-C* (see Table II) for $\Delta C = 0.1$ cycle would be

$$\Delta L = 0.1(2-6+11-21+42) = 2.8 \text{ cycles}$$

For a given base frequency error lower-side band method of tandem operation introduces no greater line frequency error than systems employing individual modulators for directly translating each voice band to its line frequency assignment.

A schematic layout of the carrier supply group is shown in Figure 8. Regular and spare supplies are identical and are energized by separate power distribution circuits for added reliability. A 3.6-kc temperature-controlled *LC* oscillator of good stability is employed to generate the system base frequency from which the carrier frequencies are derived in a conventional manner by means of a harmonic generator and appropriate harmonic selection filters.

A temperature-controlled crystal oscillator operating at 151.2 kc is employed to monitor continuously the highest translating frequency, also 151.2 kc, derived from the harmonic generator. Monitoring is accomplished by a frequency control unit, essentially a phase detector, whose output voltage is proportional in magnitude and polarity to the phase difference between the two sources. By applying this voltage to a reactance tube frequency control circuit incorporated in the base frequency oscillator, exact synchronism between the two sources is maintained. Thus the frequency stability of the entire terminal is identical to that of the crystal oscillator and well within the desired limits. Al-

though this control arrangement may be somewhat unusual because of the high degree of frequency multiplication involved (42d harmonic of 3.6 kc), properly designed damping characteristics render it completely stable in operation.

A meter in the frequency control unit indicates the amount and polarity of the correction voltage being applied to the base oscillator. An associated alarm relay gives an audible and visual indication in the event the correction voltage should exceed 50 per cent of its maximum allowable value, enabling the attendant to manually retune the base oscillator without interrupting the circuit. The crystal oscillator may be varied over a limited frequency range sufficient to compensate for differences in various crystal units and to permit frequency matching of all carrier supplies to a given standard.

A high degree of reliability is secured by providing duplicate sets of carrier generating equipment. Associated switching arrangements transfer the load from one to the other either automatically or under manual control. The carrier switching panels contain two cathode-follower amplifiers each actuated by corresponding frequencies from the regular and spare carrier supply groups. A single output transformer is switched mechanically into the cathode of the appropriate amplifier, furnishing a very low impedance carrier source to energize the carrier bus. The busses normally are energized by the regular supply and their power levels are monitored individually by meter relays. A failure on any bus causes the corresponding meter relay to operate and transfer the entire office load to the spare supply where it will remain until manually restored. The level of each carrier frequency from the idle supply also is monitored at all times to insure its readiness in case of need.

The carrier switching control panel provides visual means for indicating which supply group is carrying the office load and in the event of a failure, indicates on which group it occurred. A master switch disables the automatic feature and permits manual control to be established for testing and maintenance purposes. In addition to a visual indication, an audible alarm is sounded upon the failure of any carrier frequency, regular or spare.

A frequency distribution network inserts protective resistors between each carrier frequency bus and the multipled feeders for supplying the various *WN* systems in an office so that faults on any particular feeder group will not affect the levels on the other groups. Up to eight system terminals can operate from a single carrier supply group. The carrier frequencies are converted into the desired square wave form by appropriate amplifiers associated with the voice and block modulator groups. Each amplifier has sufficient power output to operate eight modulators.

Automatic and manual switching is also provided in connection with the regular and spare 151.2-kc crystal-controlled oscillators. Provisions are made for indicating the frequency difference between the two oscillators and for comparing them independently with a standard frequency source.

Performance

Signal-to-noise measurements on individual voice bands with all remaining bands carrying multichannel telegraph traffic averaged better than 50 decibels. This margin is quite adequate for tandem operation of as many systems as reasonably might be required to cover the continental United States and yet maintain a signal-to-noise ratio sufficiently great for telegraph and facsimile service. With the system operating over a radio relay circuit consisting of two terminals and seven repeaters, the comparable signal-to-noise ratio was approximately 43 decibels.

Observations of frequency stability over a period of several weeks indicated the maximum frequency shift in any voice band to be not greater than one-fourth cycle per second.

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No Discussion

Filters for a 150-Kc Carrier System

R. C. TAYLOR
ASSOCIATE AIEE

Synopsis: This paper describes the filters developed for use in subdividing a frequency band of approximately 150 kc into 32 voice bands, each 3,000 cycles wide. A system of modulation in which each successive stage carries twice the number of voice bands found in the preceding stage reduces to six the total number of different frequency bands requiring separation by means of filters. Economy in cost and mounting space is attained through use of filter types which minimize the number of inductive elements required. Highly stable powdered iron cores are used in all inductive elements.

IN the wire line carrier systems now widely used in the telegraph company's plant, a voice frequency channel having a band width of approximately 3,000 cycles constitutes the basic unit. This basic unit may be broken down into a number of narrower channels for teleprinter or multiplex operation or may be used as a whole for facsimile or telephone transmission. Additional bands required to utilize the available signal carrying capacities of the various systems are obtained by means of modulators. The translating carrier is suppressed and only one of the side bands resulting from the modulation process is retained. The other first-order side band and any interfering higher order side bands are rejected by suitable filters.

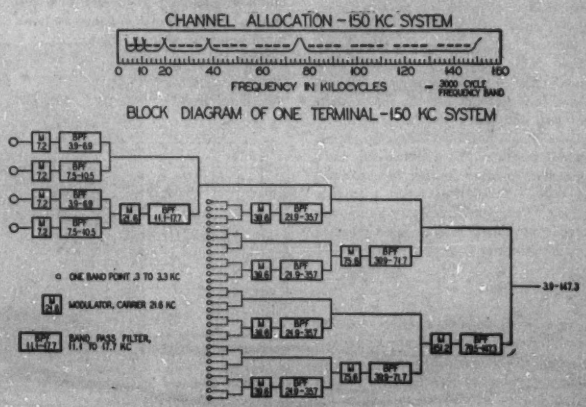
In carrier systems where the total number of voice frequency bands that must be so derived is small, the design of filters required to select the single desired side band and reject all the other unwanted frequencies has not presented difficult problems. However, in a system capable of handling 32 bands of signaling frequencies each 3,000 cycles wide and extending up to 150 kc, much more difficult filter design problems are presented. One solution is the crystal filter. Another is described here and in a current paper.[1]

In order to simplify the filter design and at the same time to utilize efficiently the 150-kc wide band and retain the 3,000-cycle voice frequency band as the basic unit, a scheme of multiple, or consecutive, modulations was adopted.[2] The resulting system has been designated as type *WN*. In this arrangement only two of the side-band selecting filters have 3,000 cycle band width, and moreover they occur at the lowest frequency positions in the system. At subsequent modulation points the filters select groups of 2, 4, 8, or 16 bands. A geometric progression in filter band widths is the result, with an approximately constant ratio between band width and location of the band in the 150-kc spectrum. Details of this system are shown in Figure 1. As the figure shows, only six different frequency bands in various parallel arrangements are required to yield a total of 32 voice bands.

All the filters have relatively wide pass bands in terms of the ratio of upper cutoff to lower cutoff. This ratio is roughly two in all six bands. For instance, the lowest frequency filter of the series has a lower cutoff of 3,600 cycles and an upper cutoff of 7,100 cycles and the highest frequency filter has a lower cutoff of 76,200 cycles and an upper cutoff of 152,000 cycles.

Figure 1 shows the useful signal frequencies as distinguished from the cutoff frequencies, and it may be observed that it is never necessary to filter out frequencies less than 5.5 per cent away from the signal. Since the various 3,000-cycle bands must be suitable for facsimile or speech transmission, a signal to noise ratio of 50 decibels or more is desirable and that value is realized throughout the system. It may be accomplished by a

Figure 1. *WN* system block diagram



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R. C. TAYLOR is with the engineering department, Western Union Telegraph Company, New York, N. Y.

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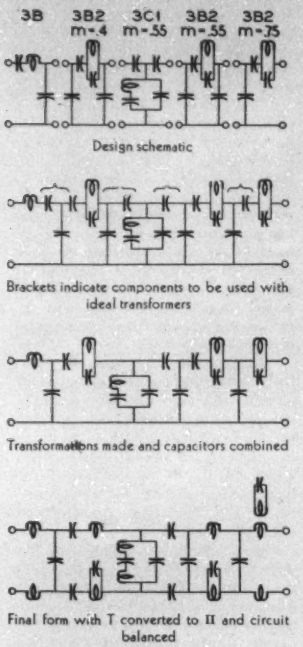


Figure 2. Successive stages in design of typical filter

combination of two filters or a combination of modulator balance and filter attenuation but the end result is interference 50 decibels below the signal.

In the design of the filters, the number of inductive elements was kept at a minimum since coils usually require more space and are more expensive than capacitors. This principle was applied most intensively to the lower frequency bands since they account for the major share of cost and space in the system. It proved to be possible to develop the required discrimination with five coils and a somewhat greater number of capacitors in each of the three lower frequency bands. The higher frequency filters were permitted more coils since used in much smaller quantity.

Typical Filter

As an example of these filters, consider the filter shown schematically in various stages of development in Figure 2 and indicated by the numbers 3.9–6.9 in Figure 1. It follows a balanced modulator fed with a 7,200-cycle square-wave carrier frequency and a 300 to 3300-cycle signal band. It must deliver the first-order lower side band of that modulation (3,900 to 6,900 cycles), with the upper side band, signal leak, and higher order modulation products suppressed far enough to result in a 50-decibel signal to noise ratio. Since the balance of the modulator may be depended on for 30-decibel suppression of the signal leak, the filter needs to supply only 20-decibel attenuation at frequencies less than the useful signal band; however, it must supply the full 50 decibels unaided at the upper side-band frequencies. Since the carrier is a square wave, the third order products ($3c \pm s$) are only about 10 decibels below the useful signal level and the filter must supply 40 decibels attenuation. It is evident that if efficient use of filter elements is to be had, a quite dissymmetrical filter characteristic is required and most of the m-derivation peaks need to be on the high frequency side. The resulting measured attenuation curve is shown together with the straight line curve of requirements in Figure 3.

The various stages of development from the design schematic to the final balanced network are shown in Figure 2. There are a number of steps in the selection of the component filter sections, and in the transformation from the design schematic to the final form which may be of interest, since many filter design sources do not list them. In order to provide a basis for discussion the filter sections considered, together with their m-derivations and design formulas, are given in Figure 10 of Appendix I. In the notation of Figure 10 the filter shown in Figure 2 is made up of five half-sections of type *3B2*, two half-sections of type *3C1*, and one half-section of type *3B* with m-values as shown in the first schematic of Figure 2.

Both the *3B2* and *3C1* half-sections consist of three capacitors and one coil and provide one peak of attenuation which meets the requirement of minimum inductive elements and permits independent assignment of peak attenuation frequencies. One peak of attenuation can be realized for every coil in a band pass filter and they may be assigned at will on either side of the pass band. (In this filter one of the peaks is at infinite frequency.) The selection of m-values and section types was done by trial and error to fit the required attenuation characteristic with the minimum number of coils, all the section types of Figure 10 being considered.

The section junctions on each side of the *3C1* section may at first sight appear to be mismatched, but according to Figure 10 the mid-shunt surge impedance of type *3B2* and the mid-series surge impedance of type *3C1* differ only by the constant factor $(1+f_1/f_2)^2$, and therefore can be perfectly matched by a suitable choice of impedance level in the *3C1* section, or by use of the "ideal transformer" technique as outlined in Appendix II.

In the *WN* system filter selected for detailed description, several capacitances are larger than is desired for economic application of mica capacitors at a 600-ohm impedance level. To reduce these capacitances, an ideal transformer was applied at four places (shown by the brackets in the second schematic of Figure 2), using the formulas of Appendix II. The result is shown in the third schematic of Figure 2 and was sufficient to reduce the capacitances to satisfactory values while leaving the terminal characteristic impedances unchanged.

The final form is shown in the fourth and last schematic of Figure 2, and is obtained from the third by converting a T of capacitors into a Π and balancing the circuit against longitudinal disturbances.

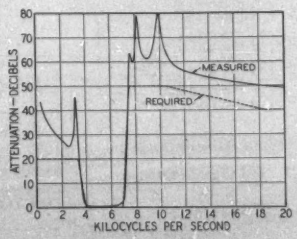


Figure 3. Required (dotted) and measured (solid) attenuation

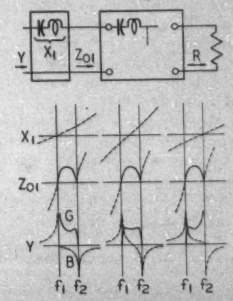


Figure 4. Negative slope susceptance in a filter pass band

Dashed line—Imaginary component
Solid line—Real component

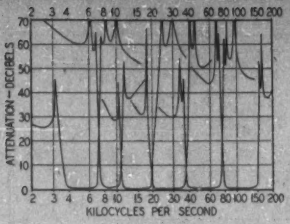


Figure 5 (left). Measured attenuation of six *WN* system filters

Figure 8. Parts of coils ready for assembly and completed coil with associated capacitors

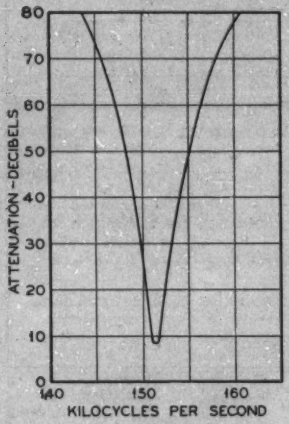


Figure 6. Measured attenuation of 151.2-kc filter

The three parallel resonant circuits in the series branches of the design schematic are not treated alike in the final schematic. The method using three windings on a common core as in the $m=0.75$ section is preferable from balance and manufacturing standpoints but introduces four times the leakage reactance in the series branch. This unwanted leakage inductance tends to reduce the attenuation when series resonant, which effect can be shifted to about twice the frequency by the method used on the $m=0.4$ and $m=0.55$ sections. The effect can be eliminated entirely by applying the tuning capacitor to both halves of the series coil but that did not prove to be necessary in this case. An alternative procedure would be to increase the amount of interleaving of the windings but that is always inconvenient for the manufacturer.

The input and output impedances of a filter are always important in determining the flatness of the attenuation-frequency characteristic in the pass band, and in five of the six frequency bands an m-derived characteristic impedance ($m=0.5$) is obtained at the line end by the fractional end branch termination required for satisfactory paralleling and described in subsequent paragraphs. The sixth filter obtains a similar result illustrated in curve *G* of Figure 4.

For the higher frequency bands a conventional 6-element symmetrical m-derived section ($m=0.55$ or 0.65) is used at the set end, but for the lower frequency bands the limitation on coils caused the use of less uniform impedances such as those described by the equation for Z_{01} of *3B2* in Figure 10. In the case of the filter of Figure 2, the set-end impedance is Z_{01} of *3B2* with $m=0.75$, which is similar to the familiar constant K impedance with which it is identical if $m=1$.

Modifications for Paralleling

Having secured a satisfactory attenuation characteristic for the individual filters it remains to make sure that the performance is not degraded when they are paralleled in service. Fortunately it is possible to make the paralleling filters improve the performance of the one being measured, as has been well described in the literature,[3] so that only a brief sketch of the method is appropriate here. The basic principle states that a constant (or nearly constant) resistance in series with a reactance of similar or smaller magnitude results in a suceptance-frequency characteristic of negative slope which can be used to resonate or cancel a conventional susceptance, not at a single frequency, but over a band of frequencies. That is, each of a group of parallel connected filters can be made to have a negative slope susceptance in its pass band where its surge impedance is resistive, but will retain a positive slope sus-

Figure 7 (left). Typical assembly—two filters on double deck rack panel

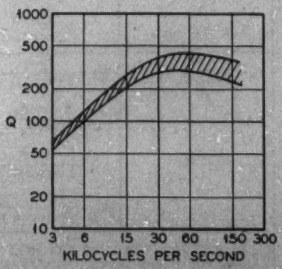
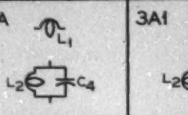
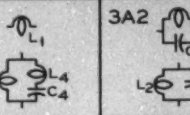
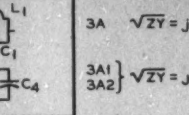
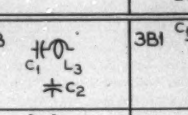
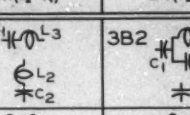
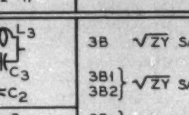
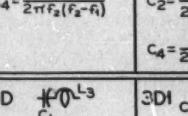
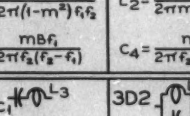
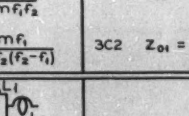
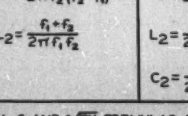
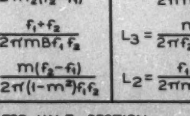
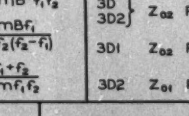


Figure 9. Coil reactance – resistance ratios realized in WN filters

PROTOTYPE	m - DERIVATIONS		SECTION PARAMETERS
3A 	3A1 	3A2 	3A $\sqrt{ZY} = j\,\frac{f}{f_2}\,\frac{\sqrt{1-(f_1/f)^2}}{\sqrt{1-(f_1/f_2)^2}} = \frac{\sqrt{1-(f/f_1)^2}}{\sqrt{(f_2/f_1)^2-1}}$
			3A1, 3A2 $\sqrt{ZY} = j\,\frac{\sqrt{1-(f_m/f_2)^2}}{\sqrt{1-(f_1/f_2)^2}}\,\frac{\sqrt{1-(f_1/f)^2}}{\sqrt{1-(f_m/f)^2}}$
			3A, 3A1 $Z_{01} = \frac{1}{1+f_1/f_2}\,\frac{\sqrt{1-(f/f_2)^2}}{\sqrt{1-(f_1/f)^2}}$
			3A, 3A2 $Z_{02} = 1-\frac{f_1}{f_2}\,\frac{1}{\sqrt{1-(f_1/f)^2}\,\sqrt{1-(f/f_2)^2}}$
			3A1 $Z_{02} = \frac{1-f_1/f_2}{1-(f_1/f_m)^2}\,\frac{1-(f/f_m)^2}{\sqrt{1-(f_1/f)^2}\,\sqrt{1-(f/f_2)^2}}$
			3A2 $Z_{01} = \frac{1-(f_1/f_m)^2}{1+f_1/f_2}\,\frac{\sqrt{1-(f/f_2)^2}}{\sqrt{1-(f_1/f)^2}\,[1-(f/f_m)^2]}$
$L_1 = \frac{1}{2\pi(f_1+f_2)}$	$L_1 = \frac{m}{2\pi(f_1+f_2)}$	$L_1 = \frac{mB}{2\pi(f_1+f_2)}$	
$L_2 = \frac{f_2-f_1}{2\pi f_1^2}$	$L_2 = \frac{f_2-f_1}{2\pi mBf_1^2}$	$C_1 = \frac{1-m^2}{2\pi m(f_2-f_1)}$	
$C_4 = \frac{1}{2\pi(f_2-f_1)}$	$L_4 = \frac{1-m^2}{2\pi mB(f_1+f_2)}$	$L_2 = \frac{f_2-f_1}{2\pi mf_1^2}$	
	$C_4 = \frac{mB^2}{2\pi(f_2-f_1)}$	$C_4 = \frac{m}{2\pi(f_2-f_1)}$	
3B 	3B1 	3B2 	3B $\sqrt{ZY}$ SAME AS FOR SECTION 3A
			3B1, 3B2 $\sqrt{ZY}$ SAME AS FOR SECTIONS 3A1 AND 3A2
			3B, 3B1 Z_{01} RECIPROCAL OF Z_{02} OF SECTION 3A
			3B, 3B2 Z_{02} RECIPROCAL OF Z_{01} OF SECTION 3A
			3B1 Z_{02} RECIPROCAL OF Z_{01} OF SECTION 3A2
			3B2 Z_{01} RECIPROCAL OF Z_{02} OF SECTION 3A1
$C_1 = \frac{f_2-f_1}{2\pi f_1^2}$	$C_1 = \frac{f_2-f_1}{2\pi mf_1^2}$	$C_1 = \frac{f_2-f_1}{2\pi mBf_1^2}$	
$L_3 = \frac{1}{2\pi(f_2-f_1)}$	$L_3 = \frac{m}{2\pi(f_2-f_1)}$	$L_3 = \frac{mB^2}{2\pi(f_2-f_1)}$	
$C_2 = \frac{1}{2\pi(f_1+f_2)}$	$L_2 = \frac{1-m^2}{2\pi m(f_2-f_1)}$	$C_3 = \frac{1-m^2}{2\pi mB(f_1+f_2)}$	
	$C_2 = \frac{mB}{2\pi(f_1+f_2)}$	$C_2 = \frac{m}{2\pi(f_1+f_2)}$	
3C 	3C1 	3C2 	3C $\sqrt{ZY} = j\,\frac{f_1}{f}\,\frac{\sqrt{1-(f/f_2)^2}}{\sqrt{1-(f_1/f_2)^2}} = \frac{\sqrt{1-(f_2/f)^2}}{\sqrt{(f_2/f_1)^2-1}}$
			3C1, 3C2 $\sqrt{ZY} = j\,\frac{\sqrt{1-(f_m/f_1)^2}}{\sqrt{1-(f_2/f_1)^2}}\,\frac{\sqrt{1-(f_2/f)^2}}{\sqrt{1-(f_m/f)^2}}$
			3C, 3C1 $Z_{01} = \frac{1}{1+f_1/f_2}\,\frac{\sqrt{1-(f_1/f)^2}}{\sqrt{1-(f/f_2)^2}}$
			3C, 3C2 Z_{02} = SAME AS FOR SECTION 3A
			3C1 $Z_{02} = \frac{1-f_1/f_2}{1-(f_m/f_2)^2}\,\frac{1-(f_m/f)^2}{\sqrt{1-(f_1/f)^2}\,\sqrt{1-(f/f_2)^2}}$
			3C2 $Z_{01} = \frac{1-(f_m/f_2)^2}{1+f_1/f_2}\,\frac{\sqrt{1-(f_1/f)^2}}{\sqrt{1-(f/f_2)^2}\,[1-(f_m/f)^2]}$
$C_1 = \frac{f_1+f_2}{2\pi f_1f_2}$	$C_1 = \frac{f_1+f_2}{2\pi mf_1f_2}$	$L_1 = \frac{m(f_2-f_1)}{2\pi(1-m^2)f_1f_2}$	
$L_2 = \frac{f_2-f_1}{2\pi f_1f_2}$	$L_2 = \frac{f_2-f_1}{2\pi mBf_1f_2}$	$C_1 = \frac{f_1+f_2}{2\pi mBf_1f_2}$	
$C_4 = \frac{f_1}{2\pi f_2(f_2-f_1)}$	$C_2 = \frac{mB(f_1+f_2)}{2\pi(1-m^2)f_1f_2}$	$L_2 = \frac{f_2-f_1}{2\pi mf_1f_2}$	
	$C_4 = \frac{mBf_1}{2\pi f_2(f_2-f_1)}$	$C_4 = \frac{mf_1}{2\pi f_2(f_2-f_1)}$	
3D 	3D1 	3D2 	3D $\sqrt{ZY}$ SAME AS FOR SECTION 3C
			3D1, 3D2 $\sqrt{ZY}$ SAME AS FOR SECTIONS 3C1 AND 3C2
			3D, 3D1 Z_{01} RECIPROCAL OF Z_{02} OF SECTION 3A
			3D, 3D2 Z_{02} RECIPROCAL OF Z_{01} OF SECTION 3C
			3D1 Z_{02} RECIPROCAL OF Z_{01} OF SECTION 3C2
			3D2 Z_{01} RECIPROCAL OF Z_{02} OF SECTION 3C1
$C_1 = \frac{f_2-f_1}{2\pi f_1f_2}$	$C_1 = \frac{f_2-f_1}{2\pi mf_1f_2}$	$L_1 = \frac{mB(f_1+f_2)}{2\pi(1-m^2)f_1f_2}$	
$L_3 = \frac{f_1}{2\pi f_2(f_2-f_1)}$	$L_3 = \frac{mf_1}{2\pi f_2(f_2-f_1)}$	$C_1 = \frac{f_2-f_1}{2\pi mB^2f_1f_2}$	
$L_2 = \frac{f_1+f_2}{2\pi f_1f_2}$	$L_2 = \frac{f_1+f_2}{2\pi mBf_1f_2}$	$L_3 = \frac{mBf_1}{2\pi f_2(f_2-f_1)}$	
	$C_2 = \frac{m(f_2-f_1)}{2\pi(1-m^2)f_1f_2}$	$L_2 = \frac{f_1+f_2}{2\pi mf_1f_2}$	

L, C AND $\sqrt{ZY}$ FORMULAS FOR HALF-SECTION PLUS OR MINUS SIGNS FOR $\sqrt{ZY}$ AND Z_0 AS REQUIRED.

L, C AND Z_0 FORMULAS FOR ONE OHM

$B = \frac{1-(f_1/f_2)^2}{1-(mf_1/f_2)^2}$

Figure 10. Design information for 3-element filter sections and their m-derivations

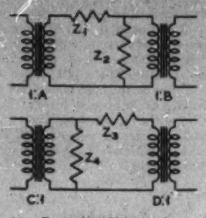


Figure 11. Equivalent L networks using ideal transformers

ceptance outside the pass band. The combination of these positive and negative slope susceptances produces a kind of antiresonance in the pass band of each filter, leaving the real part of the input impedance unshunted by the paralleling filters. Mathematically this inverted susceptance arises from the negative sign on the right-hand half of the following equation whenever R is large enough to hold the magnitude of the denominator substantially constant.

$$\frac{1}{R+jX}=\frac{R}{(R^2+X^2)}-j\frac{X}{(R^2+X^2)} \qquad (1)$$

A graphical representation of the admittance of a single filter is shown in Figure 4 for three cases; the added reactance series resonant at the lower cutoff, mid-band, and upper cutoff frequencies, respectively.

The sum of the six susceptances in the signal bands of these filters has been calculated to be less than 50 per cent of the line conductance, and similarly a departure of 25 per cent is calculated for the conductance. It is recognized that smaller tolerances than these can be obtained by more complex susceptance annulling networks but it is believed that a good economic compromise has been reached. The added series resonant series branch is included in the schematics of Figure 2, although not mentioned in the discussion of that figure, for the sake of clarity.

Measured Attenuation

The most important parts of the measured attenuation curves of the six filters are shown in Figure 5. The combined effects of dissipation and imperfect termination are about one decibel at the edge of the signal spectrum in all the filters, and any departure from a smooth curve in the central part of the pass band is less than one-tenth decibel. The result is 32 voice bands in a frequency spectrum of 143.4-kc band width or 4,480 cycles per 3,000-cycle band, a spectrum efficiency of 67 per cent. Two filters assembled on a doubledeck rack panel are shown in Figure 7.

There are a few narrow band filters in the *WN* system used to select pilot tones and oscillator harmonics that are of slightly unusual construction. They are made of *3C* sections, mid-shunt terminated, and therefore must be connected to a common source or load in series rather than in parallel. This arrangement is used to avoid the high inductance series branch coils with their troublesome low Q and humidity coefficients, and at the same time have a convenient inductive impedance transformation to obtain the most reasonable capacitances in the rest of the filter. Figure 6 shows the measured attenuation of such a filter centered on 151.2 kc and with a nominal band width of 1,200 cycles.

Coils and Capacitors

Of the available coils, the ones most suited to this work use a shell type dust core with a heavy copper shield illustrated by Figure 8. They provide a Q as shown in the shaded area of Figure 9, the variation at any one frequency depending on nearness to self-resonant frequency, wire diameter, and copper space factor. They have adequate stability when subjected to varying temperature and signal levels. The temperature coefficient of inductance is approximately plus 50 parts per million per degree centigrade, and the total change of inductance with flux density reaches one part per thousand only at levels well above the operating point. The change of Q with level is much greater, but of less importance since it is effective only at the "corners" of the propagation characteristic and any changes at normal signal levels are too small to measure.

The coils are easily manufactured with an inductance tolerance of plus or minus two per cent. Only a small percentage of rejects is found and those outside inductance limits are easily adjusted to the required value by air gap spacers.

The capacitors used are the usual commercial mica capacitors in sizes from 0.00015 to 0.07 microfarad with an average of 0.02. They are given an aging treatment by means of temperature cycles and selected in groups of three or less to values accurate to plus or minus one per cent. This requirement is placed on capacitors not associated with a peak of attenuation, and a corresponding requirement of one-half per cent of specified frequency is placed on the frequency of attenuation peaks. (The oscillator used for adjustment is required to be accurate to one-tenth per cent in order not to contribute important errors to the measurements.)

Appendix I. Three-Element Filter Sections and Their *m*-Derivations

The four well-known 3-element filter sections[3,4] are shown in Figure 10, each grouped with its two *m*-derivations and with formulas for element values, dissipationless apparent propagation constant, and surge or image impedance. This grouping of the sections is chosen to emphasize the *m*-derived relation between sections such as *3B* and *3B1*, frequently obscure in the literature, and to place on an equal basis the alternative *m*-derivation, such as *3B2* usually missing from tables of filter design formulas. This omission probably results from the rapidly varying surge impedance–frequency characteristic which, with a few exceptions, limits their use to the body of the composite filter.

All formulas for element values are given for half sections rather than full sections since it has been the author's experience that half sections are the building blocks used by the designer. The half section element equations always contain 2π in the denominator while the full section formulas have either π or 4π. (The 2π would disappear if angular velocity were used instead of frequency.) The apparent propagation constant, $\sqrt{ZY}$, for the half section is identical with the full section parameter $\sqrt{Z_1/4Z_3}$ of Guillemin[3] and Shea.[4] The square root is shown since the real propagation constant, θ, is approximately equal to $\sqrt{ZY}$ to the degree that θ approximates $\sinh\theta$.

The 3-element branches have been transformed to that canonical form exhibiting the peak forming elements as simple series resonant or parallel resonant elements, for convenience in testing or trouble shooting. Some of the formulas are more complicated when so transformed, but not prohibitively so. Convenient tables[5] are available to aid in evaluating the equations.

The element and surge impedance formulas are given for a 1-ohm filter since some economy of design effort may be realized in that way. It is understood that all in-

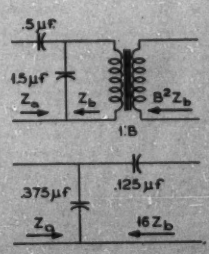


Figure 12. Example of ideal transformer use

ductances are to be multiplied and all capacitances are to be divided by the nominal circuit impedance at the end of the design work. The symbols are largely self-explanatory but for completeness the following list is given.

f_1—Lower cutoff frequency.
f_2—Upper cutoff frequency.
f_a—Frequency of peak attenuation.
Z_m—Mid-series surge impedance.
Z_{sr}—Mid-shunt surge impedance.
m and B—Design parameters related to f_a.

It may be observed from the formulas of Figure 10 that as m varies from one to zero the m-derived surge impedance varies between limits defined by the two surge impedances of the prototype. A more nearly constant result can be obtained from such sections as *3A2* and *5B1* than from *3A1* and *3B2*.

Appendix II. Ideal Transformers

The following method for using ideal transformers has been found convenient as it avoids consideration of negative elements and provides a useful tool for changing impedance levels in an otherwise completed filter design. It is similar to the discussion of pages 135–6 of reference 4.

To avoid having to reverse mentally the direction of a schematic diagram in which a transformation is desired, we can introduce four ideal transformers as in Figure 11 and then in any application retain only the one needed and cause the other three to vanish by making their turn ratios unity. If the two schematics of Figure 11 are to be equivalent then their impedance matrices[3] must be equal, term for term.

$$\begin{Vmatrix} \dfrac{Z_1+Z_2}{A^2} & \dfrac{B}{A}Z_2 \\ \dfrac{B}{A}Z_2 & B^2Z_2 \end{Vmatrix} = \begin{Vmatrix} C^2Z_4 & \dfrac{C}{D}Z_4 \\ \dfrac{C}{D}Z_4 & \dfrac{Z_3+Z_4}{D^2} \end{Vmatrix} \tag{2}$$

which leads to the following equations

$$Z_1/Z_2 = Z_3/Z_4 = AC/BD \tag{3}$$

$$ABCD = 1 + Z_1/Z_2 = 1 + Z_3/Z_4 \tag{4}$$

As an example, suppose it is desired to introduce an ideal transformer as in Figure 12. The turn ratios A, C, and D will be unity. B will equal $1 + 1.5/0.5$ or 4, and the second schematic is equivalent to the first. It should be noted that after the transformer is introduced, the series impedance is always on the high impedance side of the circuit, or in other words the configuration of the circuit determines whether the ideal transformer will step up or step down. A, B, C, and D are either unity or greater than unity.

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No Discussion

Oil Film Thickness Indicator for Journal Bearings

M. L. GREENOUGH
NONMEMBER AIEE

THE oil film thickness indicator was developed in connection with a general program of engine instrumentation for use in journal bearing studies. This instrument was specifically designed for the determination of the thickness of lubricating oil in a machine for testing experimental sleeve-type bearings. Actually the instrument shown in Figure 1 measures the clearance between shaft and bearing in all angular positions. However, its principle of operation is adaptable to many other problems involving measurement of small clearances or thicknesses.

In this particular problem, it is desired to measure film thickness in a six-inch diameter bearing with a radial clearance of about 0.006 inch. Since the rotational speeds might be in excess of 10,000 rpm, it is felt advisable to have no physical contact with the shaft, nor should the measuring system affect the bearing performance in any way. The system must operate in an atmosphere of, or submerged in, lubricating oil at 200 degrees Fahrenheit. Cleanliness of the oil is maintained by filters in the oil circulating system.

Many systems for the measurement of oil film thickness in journal bearings have been described in the literature, some of which are listed with the references at the end of the paper. Among these are mechanical-optical and optical interference methods, as well as capacitive and low-frequency magnetic systems. For the application described, however, a mutual inductance type of electrical distance measuring element appears to offer the most satisfactory solution. Its linear response makes it highly suitable for use in combination with a cathode ray tube in a system where the end indication is a polar diagram of shaft displacement from bearing center. These elements meet the necessary requirements as to linearity, frequency response, immersion in oil, and stability. The coupling between two air core coils at radio frequency is varied by the proximity of a metallic surface, such as the shaft. Since this metal is preferably nonmagnetic, the shaft is electroplated with a thin layer of copper in a band immediately under the distance-sensing elements. No electrical contact to the shaft is required.

Four distance-sensitive elements, or probes, mounted in positions 90 degrees apart immediately beyond the bearing area are used for the determination of radial shaft displacement. By impressing the signal from each probe on a corresponding deflection plate of a cathode ray tube, it is possible to recreate the polar diagram of shaft center displacement or eccentricity, magnified many times. Since means are provided in the instrument for determining the total clearance between shaft and bearing, subtracting the eccentricity from the value of radial clearance yields the oil film thickness. Inasmuch as the clearance may be altered by differential thermal expansion of shaft and bearing, provision to make such measurements during any part of the test is necessary.

Co-ordinate and Measurement System

Figure 2 shows a schematic picture of an off-center shaft. A, B, C, and D are the distances from the bearing to the circular shaft at the four probe positions. The electrical signal from each probe is in the form of a d-c voltage proportional to the spacing between bearing and shaft. Connection to the cathode ray tube is such that an increase in spacing causes a beam motion away from the corresponding probe position, with zero signal causing zero deflection from center. Thus, if the journal were the same size as the bearing, the spot on the cathode ray tube screen would lie on the center of the screen whether each probe alone or all four were connected. With the shaft free to move in the bearing, the components of shaft displacement are picked up by the probes and transformed into voltages. These voltages, when applied to the deflection plates of a cathode ray tube, cause similar displacement components of cathode ray tube beam. Because of the over-all strict proportionality between shaft and beam motions, and of the correspondence of probe and deflection plate angles, the spot position on the screen is an accurate reproduction of shaft eccentricity at all times.

The diametral clearance is the difference in diameters of the bearing and journal, and for practical purposes is equal to the sum of the clearance indications from any pair of opposite probes. In the instrument this sum is most conveniently obtained by determining the individual clearances separately, adding the two manually. Since the deflection for a single probe is not in push-pull, a factor of 1/2 relative to the deflection caused by a pair of probes is introduced. The sum of two opposite clearances obtained should be the same regardless of which pair of probes is used, and should be independent of shaft location in the bearing. For vibratory conditions, the average spot displacement could be taken for determining radial clearance.

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M. L. Greenough is with the National Bureau of Standards, Washington, D. C.

This instrument was developed in the Electronic Instrumentation Laboratory of the National Bureau of Standards as part of a program for the Navy Department, Bureau of Ships. The collaboration and assistance of J. G. Reid, Jr., H. H. Parnell, W. E. Williams, Jr., J. Johansen, and A. Soler of the Bureau of Standards, is appreciated. Grateful acknowledgment is also given to W. V. Smith and M. Levinsohn of the Engineering Experiment Station at Annapolis for their participation in the development and test program.

The scale with which clearances are read is graduated for push-pull deflection for ease in reading eccentricity. When all probes are connected to deflection plates, which is the normal connection for observing eccentricity, the deflection along a probe axis is due to the combined effect of two opposite probes. Hence, the indication for each probe alone is one-half of the actual clearance at the probe position. With all four probes connected, each to its corresponding deflection plate, the system furnishes a continuous indication of eccentricity. In the absence of vibration the spot assumes a position indicative of the eccentricity. Vibration if present causes the spot to trace some closed loop which is a replica of shaft vibration in the bearing.

Figure 3 shows how oil film thickness measurements may be made at any point on the periphery of the bearing. It has just been shown how a magnified picture of eccentricity is displayed on the cathode ray tube screen. To determine the oil film thickness at any point around the bearing, it is necessary to subtract the eccentricity vectorially from the value of radial clearance. The actual film thickness at any point is the radial clearance minus the component of eccentricity along a line drawn from bearing center to the point under study. The eccentricity also has components perpendicular to the line from bearing center to the point under study. A rotating set of rectangular coordinates, with the origin at the true bearing center is therefore required. At the point on the bearing wall, eccentricity may be considered as made up of radial and tangential components. The tangential component is zero at the point of minimum film thickness. For a study of oil film thickness it is usually not necessary to identify a point on the bearing wall to much better than one degree, or about 0.05 inch on a 6-inch shaft. Since in a typical bearing the maximum eccentricity is limited to the 0.006-inch radial clearance value, the tangential component of eccentricity is therefore much less than the normal tolerance of angular determination, and may be neglected.

Figure 4 shows the scales on the cathode ray tube screen. Around the periphery is the angular scale, calibrated in degrees measured in a clockwise direction from the reference axis. The transparent rotatable scale is mounted so that its center is coincident with that of the cathode ray tube, which also corresponds to the center of the bearing. The rotatable scale carries the axes for measuring the component of eccentricity in any desired direction. A pointer on the base line of the scale, when read against the angular scale, shows the angle of the line from bearing center to the point under test. To assist in reading the component of eccentricity along this line, perpendicular graduation lines are provided. These are located at intervals equivalent to 0.001-inch displacement of the shaft. The particular scale on this instrument is marked from zero in the center to ±0.013 inch on the outside, providing a margin beyond the required ±0.006 inch. Since these lines are 0.150 inch apart, an overall amplification figure of 150 may be ascribed to the system. It by no means represents the maximum for systems operating on these principles, but was chosen as a convenient value, meeting the requirements for this instrument.

Operation with these scales is quite simple. To measure eccentricity and the angle of closest approach, the rotatable scale is turned until the base line passes through the spot on the tube screen. The eccentricity is read on the graduations on the base line, while the angle is indicated by the location of the pointer over the angular scale. To make measurements at any other point, the rotatable scale is turned until the pointer indicates the desired angle from the reference axis. Projection of the eccentricity on the base line is again read with the same graduation lines. The radial clearance is obtained by summing the indications from two opposite probes, the proper connections being made by a selector switch. Subtracting the component of eccentricity just measured gives the oil film thickness at the point under study.

In many cases it is not feasible to mount the probes on axes which are parallel and perpendicular to the line of action of load. Rotation of the cathode ray tube, but not the scales over its screen, by an amount equal to the differences in angle between probe axes and load line, restores correctness of displacement indications.

Distance Measuring Elements

A suitable system of electrical distance measurement was developed on the principle of variation of mutual inductance

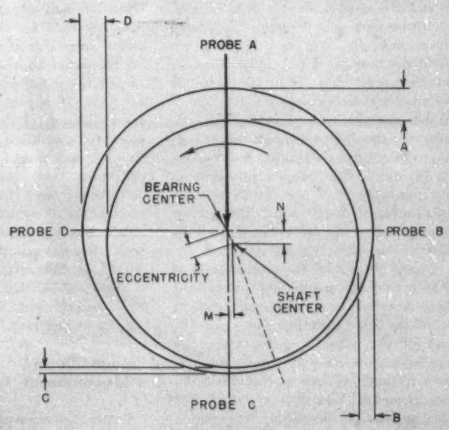


Figure 2. Co-ordinate system for determining shaft displacement from bearing center and radial clearance

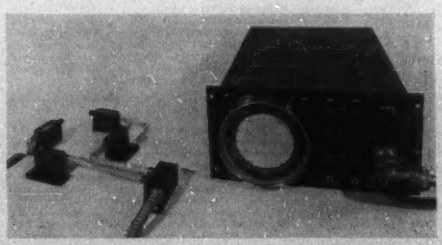


Figure 1 (left). Oil film thickness indicator for journal bearings. The four distance-sensing elements and junction box are shown, before mounting, at left

$$\text{RADIAL CLEARANCE} = \frac{A}{2} + \frac{C}{2} = \frac{B}{2} + \frac{D}{2}$$

A = RADIAL CLEARANCE + N
B = RADIAL CLEARANCE − M
C = RADIAL CLEARANCE − N
D = RADIAL CLEARANCE + M

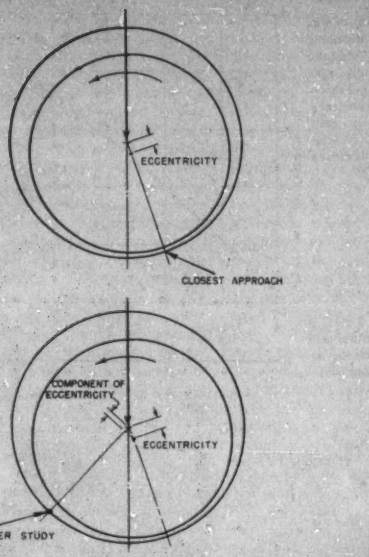


Figure 3. Method of measuring oil film thickness at any point around bearing. Shown for two different points

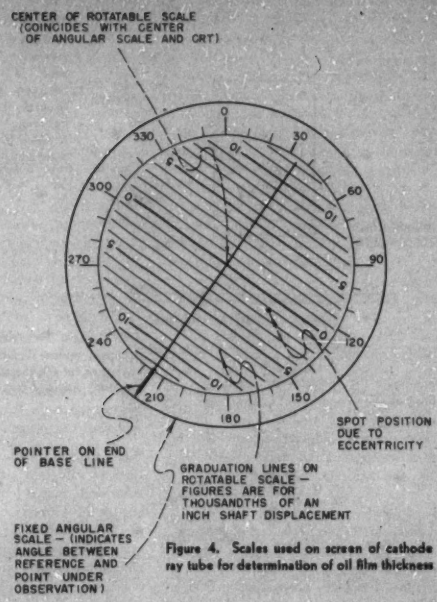


Figure 4. Scales used on screen of cathode ray tube for determination of oil film thickness

between coupled air-core coils excited at radio frequency. Suppose that a primary coil is excited by radio-frequency current, and that the plane of the coil is parallel to a nearby metal surface. Figure 5 shows such an arrangement. Let it be assumed that the metal plate is of a perfectly conducting material. Under such conditions, the electromagnetic field at the surface of the plate is exactly cancelled by the field of the eddy currents induced in the plate. A secondary or probe coil placed just at the surface has no voltage induced in it. If the probe coil is moved away from the plate toward the exciting coil, it will be found that an increasing voltage is picked up as the probe coil is placed closer to the primary coil. The voltage output of the probe coil can then be used as an indication of the gap between it and the metal surface.

Such a system has mechanical disadvantages, because of the necessity for flexible leads, for instance. An improvement is to mount both the exciting and probe coils on one form, and to move this coil assembly with respect to the metal. The same basic principles still apply. Although the strength of the exciting field is no longer constant at the metallic surface, the resulting correction to the effect of variable distance from probe coil to plate is small, so that there is not much loss in scale factor. At the same time the device becomes readily adaptable for many applications.

A typical relation of mutual inductance between the two coils versus spacing from the end of the coil form to plate is shown in Figure 6. This relation is apparently linear up to about five per cent of the diameter of the coil form. Linearity to better than one per cent is not difficult to secure over this range. One commonly useful size of coil form is one inch in diameter, permitting linear measurement up to 0.05 inch. This graph also shows the difference between the relative variations of mutual and self inductance, although the plot of self inductance may not represent optimum design for a single coil. The change of self inductance is much smaller than that of the mutual coupling, and in addition, is not linear over any appreciable range.

For several reasons, the mutual inductance is not zero at zero spacing. The probe coil cannot be located at the surface of the metal because it must have finite size and enough wall thickness for mechanical rigidity. A factor of lesser magnitude is the effective skin depth of the metal itself. The resulting initial mutual coupling may be cancelled in some instances by employing a third coil on the same form mounted close to the exciting winding, so that coupling between it and the exciting coil is relatively unaffected by the proximity of the metal. It is connected in series opposition with the probe coil, and is adjusted for zero total mutual coupling between input and output circuits when the metal surface is at the end of the coil form. When, as in this instrument, the probe coils are utilized to produce a d-c signal voltage, the initial coupling may be offset by inserting in series a bucking d-c voltage.

Considerable experimental investigation has been made of the effects of different geometrical arrangements of the coils. The characteristic shown in Figure 6 can be made concave upward or downward, as well as linear. The optimum form has been found to be two concentric, coplanar coils, one having an average diameter 0.68 to 0.70 that of the other. Maximum change of mutual inductance over the greatest linear range has been secured from this design. Since solely a mutual inductance phenomenon is involved, it makes no difference which slot

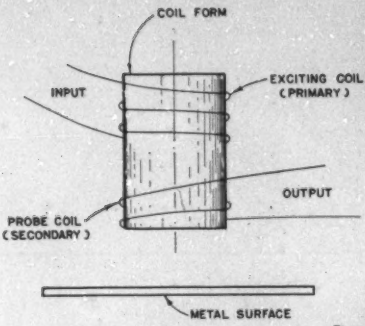


Figure 5 (left). Basic structure of distance-measuring element used in oil film thickness indicator. Mutual inductance between two coils is varied by proximity of metallic surface

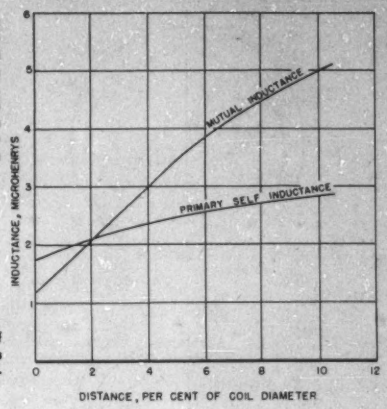


Figure 6. Variation of self and mutual inductance as used for electrical measurement of distance

is chosen for the primary winding. As long as the primary coil is always wound with the correct number of turns for the same value of inductance, provided the same procedure is followed for the secondary, the performance is identical.

It has also been found that one coil may be somewhat distributed, if the other is concentrated close to the end of the form, as shown in Figure 7*C*. This may be inferred from the cross-section views of Figure 7*A* and 7*B*, where a coplanar form is shown in 7*A*, and a "staggered" form is given in 7*B*. Again, either slot may be used for the primary winding. In spite of the slightly increased initial coupling, the greater coil area permits the use of larger wire or more turns, both of which may be advantageous when the larger slot is used for the primary, as is usually the case. The higher Q of a coil having larger wire necessitates less power input for a given value of volt-amperes. Also, the heat dissipating area is much greater. Secondary coil resistance is generally immaterial, so that the wire is usually chosen small enough to accommodate the desired number of turns.

The graph in Figure 6, showing the variation of mutual inductance as a function of distance, is for measurements to a flat metal surface. When the surface is curved, the relation is the same, except that the origin on the abscissa (distance) scale is shifted by amount equal to the effective average minimum distance. It is apparent that the initial coupling is greater, and that the useful linear range is reduced. The greater the curvature of the surface, the more pronounced are these effects. In this instrument, the curvature of a 6-inch shaft gives rise to an initial coupling equivalent to about 0.02 inch greater than that to a flat plate.

The graph in Figure 6 is also for measurements to a nonmagnetic metal surface. While magnetic materials could be used, there is a higher initial coupling and a lesser scale factor (change of voltage for change of separation). Displaying appreciable ferromagnetic properties at radio frequencies, an iron or steel base plate superimposes upon the eddy current reduction in coupling a small opposite effect as a consequence of its magnetic permeability. The combined effect is the increased initial coupling and reduced scale factor. As an example, an iron plate might cause a doubled initial coupling and a halved scale factor, so that the per cent change in mutual coupling for a given displacement is one-fourth as large. The greater stability inherently possible with devices displaying large effects makes the use of nonmagnetic surfaces desirable. As the frequency of operation is raised, the performance with iron tends to approach that with copper, but nonmagnetic materials are still preferable.

Almost any nonmagnetic metal which is considered a good conductor of electricity is suitable as a base plate or reference surface. No appreciable difference has been detected between brass and copper, for example. At present, materials of higher resistance have not been fully investigated. At a frequency of three megacycles, a metallic thickness of about 0.001 inch of copper seems to be adequate, and greater thicknesses show no visible difference in initial coupling or scale factor. However, materials having higher resistivity would undoubtedly require somewhat more depth for the same degree of performance. At higher frequencies, the necessary metal thickness is reduced.

The relatively thin layer of copper needed for good performance of this method of electrical distance measurement suggests that it would be advantageous to plate a layer of copper on the surface of magnetic materials to permit precision measurements on iron and steel at reasonably low frequencies. In the oil-film thickness indicator described this is done in order to operate at two to three megacycles. A slightly recessed groove on the steel shaft about one and one-half inches wide is electroplated with copper. This band is centered under the probes, just outside the bearing area. The copper surface is then ground and polished after plating.

Preliminary measurements indicate that the value of indicated distance is substantially independent of the dielectric constant of whatever insulating material is placed between the probe and the metal surface. Even ordinary tap water appears to have no effect. Inasmuch as the probes have to operate immersed in lubricating oil at 200 degrees Fahrenheit, this independence of dielectric constant is a necessary characteristic.

The useful electromagnetic field from the exciting coil (for measurement within the linear range) is closely confined to an area only slightly larger than the coil diameter. No further effect has been observed for increasing diameters of the metal surface above about one and one-half times that of the coil, hence the choice of a one and one-half inch width of copper band for use with the one-inch probe coils. This length of band permits axial motion of the shaft of about one-eighth inch without appreciable detriment to the accuracy of the instrument.

Electrically, this distance-measuring

element may be treated as any two coils having a variable mutual inductance, the transfer characteristic of which may be represented by a single mutual inductance if self-resonance effects are avoided. A mutual inductance bridge could be used for distance measurements, but direct excitation is more generally practicable. When a radio-frequency current is impressed upon the primary, the voltage induced in the secondary is proportional to the primary current, to its frequency, and to the mutual inductance. For a source of given current capabilities, it is desirable to use as high a frequency as possible. However, if the frequency approaches the self-resonant value for the secondary, the output voltage, while substantially increased, becomes dependent upon coil Q, stray capacitances, and critically so of the operating frequency. Obviously, for precision applications, operation near resonance is to be avoided.

Operation at lower than resonant frequency is not alone the solution, as harmonics of the exciting frequency may be accentuated, causing distortion of the linear relation of voltage output with distance. In order to attenuate the resonance rise, a simple low-pass filter, comprising a series resistor working in conjunction with the rectifier shunt capacitance, has been found to reduce it to negligible proportions. Experience has shown that excitation at about one-half the secondary resonant frequency, together with a filter limiting the rise to 50 per cent, is quite satisfactory.

The output of the distance-measuring device may be in the form of a variable amplitude radio-frequency (RF) signal, a d-c voltage, or a low-frequency alternating current. For the first, the probe output is used directly. A diode rectifier is used for the last two. The low-frequency alternating current can be produced by amplitude modulation of the exciting RF current at the frequency desired. Directly indicated measurements of air gaps and insulator thickness require a regulated RF current for excitation of the primary coil, and a diode peak rectifier and d-c voltmeter for the secondary circuit. Regulation of primary current is necessary in order to maintain stability of scale factor in the instrument, since the output readings are proportional to primary excitation. Secondary voltage is rectified by a diode contained in the probe and its output may then be fed to a d-c voltmeter calibrated in distance units.

The output obtainable is limited chiefly by the power dissipation in the primary winding. With a voltage-doubler rectifier, it is easily possible to secure an output in excess of 150 volts direct current for movements of 0.05 inch. This is over three volts per thousandth of an inch, and is a representative value for reasonable excitations, such as can be supplied by one-tube oscillators in continuous-wave operation. If a pulsed RF carrier is applied, with a peak rectifier on the secondary, the output voltage may be raised considerably for equal average primary power. It should be noted, however, that the application of the concentrated electromagnetic field required for high output voltages results in some heating of the base plate. In view of the operating ambient temperature of 200 degrees Fahrenheit at the probes in the bearing test machine, it was felt that an additional few degrees rise at the outer surface of the shaft would be inconsequential

Complete Instrument Assembly

The four probes are mounted on the end of the bearing in positions 90 degrees apart. All leads to the probes pass through a junction box, which also contains the constant-current rectifier. To facilitate removal, the probes are wired from the junction box in two strings of two.

The probe coils are wound on bakelite forms having an outside diameter of one inch and a cross section approximating that shown in Figure 7*C*. These are attached to cored-out steel blocks which also serve as the mounting base. In the

Figure 7. Cross section of several probe coil forms. The average inner diameter is approximately 70 per cent of the outer for all three types. Note larger outer slot in (c).

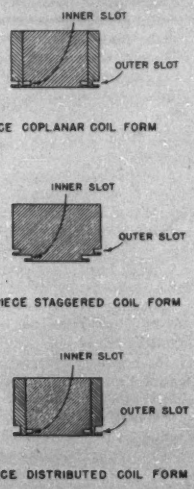
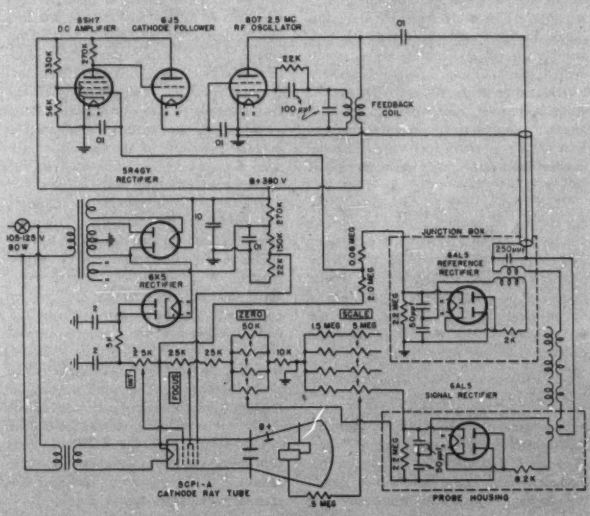


Figure 8. Schematic wiring diagram for oil film thickness indicator. For simplicity, only one of the four similar probe circuits is shown in detail



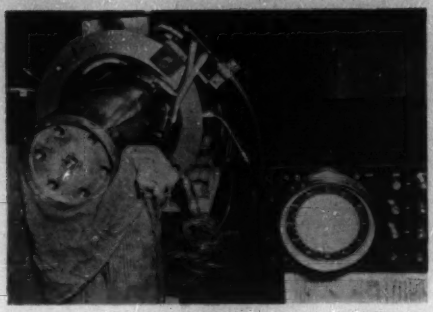


Figure 9. The Instrument as Installed on a test sleeve-type bearing. Photographed by United States Naval Engineering Experiment Station

cavity of each block are located the *6AL5* miniature voltage-doubler rectifier and associated resistors and capacitors. The probes are mounted on the bearing housing, overhanging the end of the bearing by about three-quarters of an inch to the center of the coil form. Centered under these is the electroplated copper band. To avoid the possibility of the shaft rubbing on the probe face, the probes are located so that the faces are about 0.010 inch from the shaft when the shaft and bearing are touching. This additional gap is compensated electrically in the calibration procedure.

Since the probe assembly must operate at temperatures in the order of 200 degrees Fahrenheit, the choice of interconnecting wire requires consideration. Glass-insulated wire was finally used, and is protected by temperature-resistant plastic sleeving. The cable from the junction box to the electronic unit is made up of a number of glass-insulated wires inside flexible metal tubing. Excitation power for the probe primaries is fed through a co-axial beaded cable having a short length of temperature resistant shielded lead at the junction box end. The co-axial cable is also located inside the metal tubing.

The electronic unit houses the cathode-ray tube, the oscillator and associated regulator, and the power supply circuits. The cabinet, finished in black wrinkle, is approximately 12 inches wide, 7 inches high, and 18 inches deep. Operation is on ordinary 105- to 125-volt, 60-cycle power lines, with about 80 watts power consumption.

Circuits

The circuit diagram of the oil-film thickness indicator is shown in Figure 8. The essential elements are the radio frequency oscillator, the regulator for holding constant RF output current, and the cathode-ray tube. The power generation circuit consists of an oscillator at 2.5 megacycles, using an 807 beam power tube. The necessary oscillation feedback from plate to grid is provided by a step-down transformer in the instrument. To supply as much power as possible to the exciting coil and to reduce the number of tuned elements, the exciting winding is made part of the oscillator plate tank coil itself. Directly in series with the exciting coil is a current transformer for obtaining an indication of output current. Since its output current, rather than voltage, is monitored, changes in lead length and resistance of the windings have negligible effect upon the magnitude of the probe coil outputs. The combination of exciting coil and low voltage winding on the current transformer is parallel-resonated by the tank condenser, so that the tube itself has to supply only a small fraction of the current in the load. In this instance the Q is in the order of 20. Most of the losses causing such a relatively low Q arises from the close proximity of the exciting coils to the copper-plated band on the shaft. Approximately half, or 250 micromicrofarads, of the tuning capacitance is that of the cable between the electronic unit and the probe coils.

The current transformer has sufficient step up in its turn ratio that 165 volts direct current is obtained from a full-wave doubler rectifier contained in the probe junction box. The d-c output is compared to a negative reference potential by a precision voltage divider, with the difference being fed to a direct coupled amplifier. Through a cathode follower, this controls the screen grid voltage on the power oscillator. The result is that the oscillator output current is regulated very closely to the negative reference potential.

The cathode ray tube, with its four deflection elements in quadrature, is ideally suited for displaying the polar diagram of radial shaft displacement. However, the sensitivity of an electrostatic deflection type of tube is inversely proportional to the second anode accelerating voltage; that is, the voltage required for a given deflection is proportional to the accelerating voltage. Now if the oscillator output is regulated to follow the variations of anode accelerating potential, a rise in this potential will cause a similar rise in probe output, so that the deflection remains constant. In this manner, the necessity for regulation of second anode potential of the cathode ray tube is obviated.

The oscillator control circuit has high gain in order to provide extreme constancy of deflection. If the regulator feed-back loop is broken, and a small voltage change introduced into the circuit in a forward direction, this increment appears amplified 200 to 300 times at the other side of the break. This may be considered as the figure of loop gain for the circuit. Effects due to supply voltage variations and the like are attenuated by this factor.

The probe secondary circuit comprises the low-pass filter resistor and the full-wave voltage doubler rectifier. A dual diode *6AL5* is used in each probe. The rectifier output is fed back to the electronic unit where a controllable d-c bucking voltage is introduced for zero adjustment. The difference is proportional to the spacing from bearing wall to shaft surface, when properly set. Since all probes are not exactly similar, and since deflection sensitivities on cathode ray tube plates differ, scale factor controls in the form of potentiometers are inserted between the difference voltage terminals and the deflection plates. The zero and scale factor controls are located on the front panel, and are slotted for rotation with a screw-driver.

Calibration

A selector switch permits examination of each probe alone, as well as of the combination of all four. The individual positions are used in the calibration procedure, while the normal operating position is with all four probes connected to the deflection plates.

Calibration of the instrument is quite simple, although provision must be made in the bearing mounting to move the shaft in the bearing by means of a hoist or jacks. The shaft is held against the bearing wall immediately under each probe in turn. The zero-set control for each probe is then adjusted so that the spot on the cathode ray tube is at the center of the screen. Since the shaft-

bearing clearance is known precisely from micrometer measurements, this figure will be the spacing between shaft and bearing at the location diametrically across from the point of contact of shaft and bearing. The scale factor control of the probe across from the point of contact is therefore set for the proper reading on the rotatable scale. As discussed before, the single probe deflection factor is one-half, so that the control is manipulated for an indication of one-half this total clearance. When these adjustments have been made for all four probes, the instrument is completely calibrated.

The two other positions on the selector switch should be mentioned. The counterclockwise stop disconnects all deflection circuits and ties the plates to ground, with the result that the spot then lies at its undeflected position. This must coincide with the center of the rotatable and angular scales on the front panel. Four screws projecting inward through the bezel rim on the panel serve as adjustable clamps for the screen end of the tube. The range of adjustment is normally sufficient to take care of tube to tube differences in undeflected spot position.

The extreme clockwise position of the switch adds a capacitor to each of the deflection circuits of the cathode ray tube. Its purpose is to remove vibratory components of displacement, in order to ascertain true average shaft displacement.

Performance

Figure 9 shows the instrument as used for oil film measurements on a test sleeve bearing. Three of the four probes and the junction box may be seen mounted in place around the shaft. The location of the junction box is determined largely by convenience and the length of the leads to the probes. On the panel of the instrument may be seen the calibrating controls, accessible by a screwdriver, and the cathode ray tube screen. The angular scale is visible around the periphery of the tube screen.

At the date of writing, not a great deal of test data on bearings is available. The machine for which the oil film thickness indicator was designed has displayed very little vibration, with the result that changes in load simply cause the spot to shift to a new location and remain motionless. Photographic recording appears unnecessary. Vibratory conditions have been set up in dummy shafts however, and from qualitative observations appear to be satisfactorily reproduced. The effective bandwidth of the probe outputs is in the order of 2,000 cycles per second, so that the higher frequency components of vibration can be included in the presentation.

Reliable figures of accuracy are not yet available and indeed would be hard to secure. During the first tests of the equipment, it was found that the loop gain of the regulator circuit was inadequate to compensate completely for the temperature change in copper resistance of the coils. The calculated change is about 35 per cent; this causes a corresponding decrease in coil Q, necessitating a higher oscillator screen grid voltage. It was at this time that the present step-up current transformer was introduced. The increased loop gain brought the over-all change at 220 degrees Fahrenheit down to a value equivalent to less than 0.0002 inch inward motion of the probes.

Due to the use of a bakelite coil form, there is an actual inward motion of the probes when subjected to the elevated temperatures. The thermal expansion of bakelite, being higher than that of steel, gives rise to a differential expansion of about 0.0003 inch.

The combination of these two sources of error is approximately one-half thousandth of an inch. Experiments now in progress indicate that this change may be compensated to a considerable extent by employment of a temperature sensitive resistor as a part of the scale factor control circuits in the junction box. However, all probes are not at the same temperature. There appear to be other anomalous variations, all of which, together, lead to an assumption of an over-all accuracy figure of better than 0.0005 inch.

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Discussion

J. F. Kalbach (Scientific Laboratory, University of California, Los Alamos, New Mexico): In describing a tool for obtaining new fundamental data on bearing operation, M. L. Greenough has provided another example of how electrical techniques can assist developments in many diverse fields.

If the oil film thickness indicator, as described, is used to study shaft vibrations, the closed loop observed upon the oscilloscope screen will represent the motion of the shaft relative to the bearing housing. In many instances, however, the bearing housing is not absolutely rigid, and is a part of the vibrating system. In such instances, there may be some advantage to mounting additional probe coils on a substantial support isolated from the bearing housing. This arrangement would give an indication of shaft motion in space which is desirable for checking vibration calculations.

It might also be pointed out that the oscilloscope circuit could be easily modified so that spot brilliance could be modulated at a suitable adjustable frequency. The closed loop of the oscilloscope trace would then be broken into a series of dashes by timing marks which would be useful for computing the velocity of shaft movement at any point along its vibrational path.

M. L. Greenough: In connection with Kalbach's remarks, it should be mentioned that this instrument was specifically designed to measure oil film thickness, and incidentally, any attendant vibration, since the interest was in shaft to bearing clearances.

A mutual inductance micrometer which was set up for noncontacting vibration indication showed the feasibility of using the device for such measurements, however. The frequency bandwidth of vibrations which may be measured is limited only by the time constants of the rectifier filter circuits, since the micrometer is driven at a carrier frequency of several megacycles.

Time marks obtained by blanking of the cathode ray tube beam would be quite useful when photographic recording is desired. To avoid overlapping of succeeding traces and resulting confusion, the length of exposure time must be the same as the complete period of the vibration pattern.

Power Costs and the Contribution of Existing Transmission Networks Towards Cost Reduction of Power for Electrification of Main Line Railroads with Moderate Traffic

LLEWELLYN EVANS
FELLOW AIEE

Introduction and Conclusions

THIS PAPER is one of a symposium on the subject of electrification of main line railroads having medium traffic and depends on a paper by R. L. Kimball and J. G. Holm[1] on the subject of railroad electrification energy conversion and transmission costs for a full description of the railroad and the electric equipment. The symposium gathers the facts and costs on the premise that main line railroad electrification can profitably be extended to include medium traffic railroads and should not necessarily be confined to the field of heavy traffic and heavy grades. Power costs are the theme of this paper which sets up and distributes the estimated electricity requirements and computes the bills for the four conditions covered by the Kimball-Holm paper; namely, a-c 25-cycle scheme with 1940 traffic as 100 per cent and present traffic as 157 per cent of normal, and like estimates for a d-c system. The results show that the total cost of power is of the same order as the capital charges for the trackside construction and substation as predicted by Kimball and Holm,[1] but the average price for power varies from 0.5 cent to 2 cents per kilowatt hour, depending on the load factor of the individual stations and the source of power. Conditions such as regenerative braking on grades, careful dispatching of trains, and so forth, can be chosen so as to earn a desirable price for power. The annual cost of building and operating transmission lines to combine metering stations is more than twice as much as the extra demand charges at the supply points.

To make realistic a set of conditions to go with the companion paper, the 330-mile double track railroad in the Kimball and Holm paper[1] has been set down so as to traverse the territory of three utilities with power connections assumed at four stations for alternating current, or six stations for d-c supply. The trains run with the following travel pattern: "For 100 per cent traffic, each of these fleets consists of four trains, and for 157 per cent, of five trains." In both the cases the spacing between trains is eighteen miles and the fleets are preceded and followed by uniform train movements every forty miles; thus "bunching" of trains for the two traffic levels takes place over 54 and 72 miles respectively. The demands per train at 25 cycles alternating current are 7,500 kw *A* to *D* (see Figure 1), and 5,000 kw *D* to *F*; and for d-c power to trains, 7,040 *A* to *D* and 5,000 *D* to *F*. Two traffic levels are assumed, 6,065,000,000 ton miles, 100 per cent for 1940 and 157 per cent of this amount for the current traffic level. The power requirement has been estimated at 37,000 kilowatt-hours per million ton miles with 12 per cent added for locomotives. The kilowatt-hours are distributed between stations by allowing for the grades and the speeds and according to the number of miles

Table I. Summary of Estimated Power Bills, Unit Prices and Load Factors

	A-C 100 Per Cent Traffic	A-C 157 Per Cent Traffic	D-C 100 Per Cent Traffic	D-C 157 Per Cent Traffic
With No Diversity Between Trains				
Total power bills	2,541,084	3,419,100	2,811,444	3,568,728
Energy charges	1,046,604	1,634,580	1,054,044	1,666,848
Demand charges	1,494,480	1,784,520	1,757,400	1,901,880
Total kilowatt hours (1,000s)	218,269	342,672	206,172	323,712
Sum of the station demands	127,000	146,600	142,900	153,500
Sum of station demands for maximum month	159,000	183,000	178,900	191,500
Average monthly simultaneous demand	112,000	124,000	100,000	116,000
Maximum monthly simultaneous demand	140,000	155,000	130,000	145,500
Average price per kwhr, cents	1.165	0.997	1.365	1.101
Average load factor based on billing demands	19.2	26.7	16.5	20.5
Average load factor based on simultaneous demands	22.2	31.4	21.6	31.8
With a Diversity of 2.0 Between Trains				
Total bills	1,823,376	2,471,028	1,974,732	2,717,892
Energy charges	1,042,932	1,587,828	1,046,460	1,618,848
Demand charges	780,444	883,200	928,272	1,099,044
Total kilowatt hours	218,268	342,672	206,172	323,712
Sum of the billing demands	64,180	73,300	75,120	77,580
Average monthly simultaneous demand	56,000	62,000	54,500	58,000
Maximum monthly simultaneous demand	70,000	77,500	68,000	72,500
Average price per kwhr, cents	0.884	0.686	0.964	0.833
Average load factor based on billing demands	37.3	53.4	31.3	48.8
Load factor based on simultaneous demands	44.5	63.3	43.2	63.7

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LLEWELLYN EVANS is chief consulting electrical engineer of the Tennessee Valley Authority, Chattanooga, Tenn.

R. B. George, electrical engineer for TVA, had made several studies of the railroad electrification problems in the Southeast and was good enough to read this paper and make some useful comments. O. S. Wessel checked computations in some of the cases of the paper. R. L. Kimball and J. G. Holm have given us the benefit of their judgment on the choice of some assumptions. We have been collecting railroad electrification data for some years at our TVA office, and these files have been used freely.

Table II. Estimated Power Bills for A-C and D-C 330-Mile Electrified Railroad at 1940 Traffic Level, 100 Per Cent, and at 157 Per Cent

Stations *A*, *C*, and *a*, *b*, *c* Are Supplied by a Hydro Network
Stations *E* and *d*, *e*, by a Small Steam Network
Stations *F* and *f* by a Large Steam Network
Demands Are for an Average Month, 80 Per Cent of Maximum Month

Station	Demand Kw	1,000 Kwhr Per Month	Demand Charge	Energy Charge	Total Bill	Cents Per Kwhr	Load Factor Per Cent
			100 Per Cent Alternating Current				
A	28,500	2,898	23,000	7,705	30,705	1.059	13.
C	48,000	7,560	43,300	19,360	62,660	0.828	21.6
E	33,400	5,700	41,750	45,935	87,685	1.538	23.3
F	17,200	2,031	16,490	14,217	30,707	1.511	16.2
	127,100	18,189	124,540	87,217	211,757		
12 months		218,268	1,494,480	1,046,604	2,541,084	1.165	19.2
	112,000 (Simultaneous)						22.2
			157 Per Cent Alternating Current				
A	35,000	4,550	31,600	11,835	43,435	0.954	17.8
C	50,300	11,869	45,370	30,133	75,503	0.636	32.3
E	40,600	8,949	50,750	71,927	122,677	1.383	30.
F	20,700	3,188	20,990	22,320	43,310	1.358	21.1
	146,600	28,556	148,710	136,215	284,925		
12 months		342,672	1,784,520	1,634,580	3,419,100	0.997	26.7
	124,000 (Simultaneous)						14.
			100 Per Cent Direct Current				
a	26,500	2,074	23,950	5,645	29,595	1.428	10.7
b	27,900	3,145	25,210	8,323	33,533	1.066	15.5
c	26,000	3,832	23,500	10,041	33,541	0.875	20.2
d	24,800	3,602	31,125	29,153	60,278	1.669	20.
e	18,700	2,588	23,375	21,037	44,412	1.717	19.
f	19,000	1,940	19,290	13,638	32,928	1.697	14.
	142,900	17,181	146,450	87,837	234,287		
12 months		206,172	1,757,400	1,054,044	2,811,444	1.365	16.5
	109,000 (Simultaneous)						21.6
			157 Per Cent Direct Current				
a	26,500	3,256	23,950	8,601	32,551	0.999	16.8
b	28,500	4,938	25,750	13,054	38,804	0.785	22.9
c	28,500	6,017	25,750	15,502	41,252	0.685	28.
d	28,400	5,655	35,500	45,580	81,080	1.433	27.3
e	22,600	4,063	28,250	34,837	63,087	1.552	24.7
	19,000	3,047	19,290	21,330	40,620	1.334	22.
	153,500	26,976	158,490	138,904	297,394		
12 months		323,712	1,901,880	1,666,848	3,568,728	1.101	20.5
	116,000 (Simultaneous)						31.8

Table III. Estimated Power Bills for A-C and D-C 330-Mile Electrified Railroad at 1940 Traffic Level, 100 Per Cent, and at 157 Per Cent

Stations *A*, *C*, and *a*, *b*, *c* Are Supplied by a Hydro Network
Stations *E* and *d*, *e*, by a Small Steam Network
Stations *F* and *f* by a Large Steam Network
Demands Are for an Average Month, 80 Per Cent of Maximum Month

Station	Demands No Diversity	Demands Diversity = 2.0	1,000 Kwhr Per Month	Demand Charge	Energy Charge	Total Bill	Cents Per Kwhr	Load Factor Per Cent
				100 Per Cent Alternating Current				
A	28,500	14,080	2,898	12,772	7,705	20,477	0.706	29.1
C	48,000	24,000	7,560	21,700	19,360	41,060	0.543	66.
E	33,400	16,700	5,700	20,875	45,935	66,810	1.172	46.6
F	17,200	9,400*	2,031	9,690	13,911	23,601	1.162	29.5
	127,100	64,180	18,189	65,037	86,911	151,948		
12 months			218,268	780,444	1,042,932	1,823,376	0.884	37.3
	112,000	56,000 (Simultaneous)						44.5
				157 Per Cent Alternating Current				
A	35,000	17,500	4,550	15,850	11,835	27,685	0.608	35.5
C	50,300	25,150	11,869	21,735	30,133	51,868	0.437	54.7
E	40,600	20,300	8,949	25,375	71,927	97,302	1.087	60.7
F	20,700	10,350	3,188	10,640	18,424	29,064	0.911	42.3
	146,600	73,300	28,556	73,600	132,319	205,919		
12 months			342,672	883,200	1,587,828	2,471,028	0.686	53.4
	124,000	62,000 (Simultaneous)						63.3
				100 Per Cent Direct Current				
a	26,500	14,080	2,074	12,772	5,645	18,417	0.887	20.2
b	27,900	14,080	3,145	12,772	8,323	21,095	0.671	30.6
c	26,000	14,080	3,832	12,772	10,041	22,813	0.595	37.3
d	24,800	14,080	3,602	17,600	29,153	46,753	1.325	35.2
e	18,700	9,400*	2,588	11,750	21,037	32,787	1.264	37.8
f	19,000	9,400*	1,940	9,690	13,006	22,696	1.169	28.4
	142,900	75,120	17,181	77,756	87,205	164,561		
12 months			206,172	928,272	1,046,460	1,974,732	0.964	31.3
	109,000	54,500 (Simultaneous)						43.2
				157 Per Cent Direct Current				
a	26,500	14,080	3,256	12,772	8,601	21,373	0.656	32.
b	28,500	14,250	4,938	13,825	13,054	26,879	0.544	42.5
c	28,500	14,250	6,017	13,825	15,502	29,327	0.487	58.2
d	28,400	14,200	5,655	17,750	45,580	63,330	1.119	54.5
e	22,600	11,300	4,063	14,125	34,837	48,962	1.231	58.2
f	19,000	9,500	3,047	19,290	17,330	36,620	1.201	44.2
	153,500	77,580	26,976	91,587	134,904	226,491		
12 months			323,712	1,099,044	1,618,848	2,717,892	0.839	48.8
	116,000	58,000 (Simultaneous)						63.7

* Bottom limit for two trains.

included halfway to the next station. The demands have been arrived at by bunching the trains at each station successively and dividing the loads between stations inversely proportional to the distance. To give as high a load factor as practical to each station, the load has been limited by shifting to other stations to the extent that adjacent stations could carry the overflow. To this reduced load, 2,500 kw was added for tolerance in governing at a-c station and 1,666 kw for direct current. Two situations have been considered and monthly bills computed for 100 per cent and 157 per cent traffic for both a-c and d-c systems.

The first tabulation assumed that all the trains were running full loaded all the time, while the second is an estimated bill with a diversity of two between trains. The last diversity assumption provides for trains standing on sidings, going down grade, and traveling partly loaded.

In distributing the kilowatt-hours and demands over the months of the year, the highest consumption has been taken at 150 per cent of average and the lowest at 50 per cent of average month, while the highest demand has been estimated as 125 per cent of average and the lowest at 75 per cent of average. A demand of 80 per cent of maximum and a consumption of one twelfth of the annual kilowatt-hours proved to be a medium bill for reducing the work of computation.

The rate schedules chosen are taken from the Federal Power Commission's compilation and are the lowest applicable rate published in each territory where metering stations are assumed to feed the railroad from the utility networks. For stations *A*, *C*, and *a*, *b*, *c* (see Figure 1), the demand charge is 90 cents a kilowatt and the energy charge 0.25 cents per kilowatt-hour. At stations *E* and *d*, *e*, the demand charge is $1.25 and the energy charge 0.8 cent per kilowatt-hour. At stations *F* and *f*, the demand charge is $1.00 and the energy charge 0.7 cent for first 120 hours use of the demand and 0.5 cent for the remainder. In all three schedules the small blocks and items which do not apply are not listed. These items affect the result less than one per cent but nevertheless have been computed in arriving at the figures in Tables I, II, and III.

Combining stations has no merit. Comparative estimates are made as follows: By joining *a*, *b*, *c* metering points with a transmission line, the reduction in demand cost would be the difference in demand charges for simultaneous and separate demands. In the 100 per cent d-c case without diversity, this is 13,305 kw at 10.80 a year or $133,094; and by

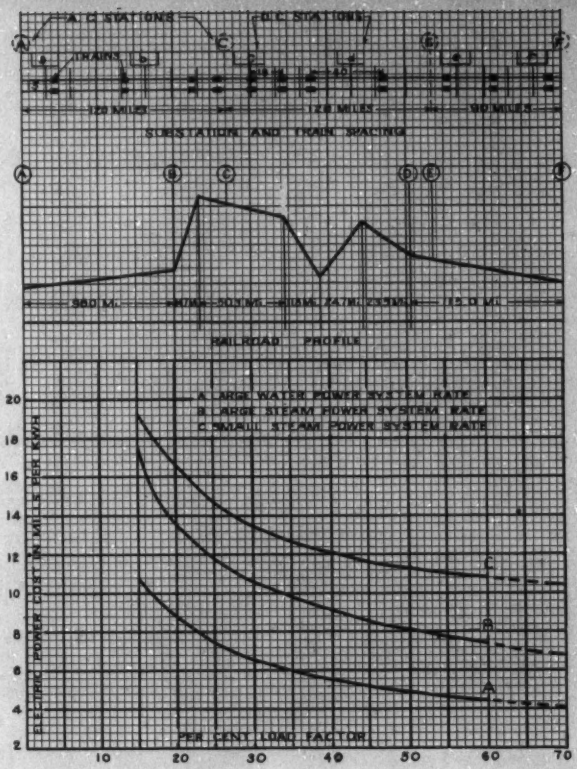

Figure 1

combining *e*, *f*, *d*, 4,177 kw at 15, or $62,650, making a total of $195,744 a year. It would take 120 miles of 100 kv line in each case costing $2,400,000 plus 70,000 kw capacity of high voltage substation capacity in each case, costing $1,400,000, total investment $3,800,000 for which the annual charges and operation would be $380,000, indicating positive advantage in buying power along the railroad from utility networks at advantageous points. A study of the tabulated cases and the rate curves shows sharp penalities for low-load factors. The annual bill drops from $3,568,728 at 20.5 per cent down to $2,717,892 at 48.8 per cent in the d-c 157 per cent cases.

The cases for which prices are computed cover a range which makes it possible to calculate by proportion the costs for others.

The most likely operating conditions on the 330-mile road in question, indicate an annual cost of $1,823,376 at an average rate of 0.884 cent per kilowatt-hour for 100 per cent traffic and $2,471,028 with an average rate of 0.686 cent per kilowatt-hour at 157 per cent of traffic. Possibly 10 per cent lower costs could be earned by careful dispatching and locomotive operation. Much higher costs could also result from train schedules and dispatching which disregard system peaks.

The problems of power contracts and rates are covered in AIEE literature.[2,3]

References

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2. Power Supply for Railroad Electrification and Fundamentals of Power Contracts, E. R. Hill. AIEE Transactions, volume 59, 1940, September section, pages 560–562.

3. Railway Power Supply, Philip Torchio. AIEE Transactions, volume 59, 1940, pages 550–3.

Discussion

T. M. C. Martin (Bonneville Power Administration, Portland, Ore.): Tables I and II and Figures 1 and 2* of the discussion comprise a brief analysis of the author's paper and the paper by Kimball and Holm.[1] This analysis is based on average monthly data applicable to the 330-mile section of railroad set up by Kimball and Holm. Unlike their paper, however, this analysis will, for convenience of calculation, assume that freight traffic is balanced by direction. This is not a major change since their paper assumed only a slight difference in eastbound and westbound tonnage. The total annual freight traffic mentioned in the subject papers (for their 100 per cent traffic case only) was given as 5,118,500 thousands of gross ton miles, and passenger traffic on the same basis was given as 947,000 thousands of gross ton miles. This is equivalent to the following average daily traffic, including movements in both directions:

Freight.....42,500 gross tons trailing
Passenger.....7,860 gross tons trailing

It was stated that although this was supposed to be a railroad of "moderate" traffic density that trains would be "fleeted" in both directions, presumably simultaneously, and although not specifically stated, it is believed that the intention was to have this "fleeting" operation apply to both passenger and freight trains. It seems impossible to account for peak demands of the magnitude shown in Evans' paper in any other way.

Accepting the principle of "fleeting" as stated before, Figure 1 of the discussion was prepared. Some locomotive had to be assumed and the one selected was the Great Northern's *5018 class*, but geared for 75 miles per hour top speed and a continuous speed of about 17 miles per hour. By using two of these units on maximum tonnage freight trains, and by running over the line at the speeds used by Kimball and Holm, it was possible to account for a peak demand of about the same magnitude as calculated by Evans. It should be noted, however, that this is fleeting with a vengeance. The average daily energy requirements for this method of operation are 673,560 kilowatt-hours, the simultaneous peak demand is 127,740 kilowatts and the load factor therefore about 22.0 per cent.

To illustrate what can be done with a little less fleeting, Figure 2 of the discussion was prepared. Two important operating changes were made:

1. Freight trains were slowed down somewhat on the ruling grade in order to permit a better locomotive tonnage rating.

2. Passenger trains though fleeted in both directions were not dispatched simultaneously from both ends of the section.

It was found possible to take all the freight tons to be moved in ten trains each of 4,250 gross tons trailing with one 360 ton locomotive. These trains were spaced out on a reasonable basis through the day. The result is to reduce the peak demand to 40,300 kilowatts and raise the load factor to 58.2 per cent.

The advantages of operations patterned along the scheme of Figure 2 of the discussion, are obvious so far as the power supplier is concerned. It is believed that the matter doesn't end there however. It seems doubtful if any railroad would be justified in committing itself to an operation of the kind represented in Figure 1 of the discussion. This would be true whether the motive power were electric, diesel-electric, steam, or any other kind. Based on average daily traffic it is evident that 28 Great Northern *5018 class* locomotives would be required to handle the assignments of Figure 1, while 12 of exactly the same kind of locomotives would provide equal protection for the assignments of Figure 2. Further, if the section is to be electrically operated it is obvious that there is a wide difference in the cost of frequency changer stations and trackside substations between the two plans of operation. It may be doubted that many railroads with the kind of traffic contemplated here can indulge in fleeting operations of this sort. They generally lack track capacity and yard facilities to enable such concentrations of activity, with their attendant bottlenecks of all kinds even if they could afford the lush supply of substation capacity and motive power which would be required.

The foregoing analysis is not meant to prove that Figure 2 of the discussion represents the way to do the job. What is intended is the suggestion that the interests of power suppliers and railroads do not run quite so contrary as might be inferred from Figure 1. It seems fair to conclude that a railroad with but a moderate traffic density need not foreclose all likelihood of achieving a good power rate by insistence upon a fleeting operation of maximum tonnage freight trains that is inconsistent with the best interests of its power suppliers and of largely illusory value to itself. Based upon

* Computations for the tables and figure of this analysis are only approximate. A slide rule was utilized freely and many quantities were rounded off to nearest hundreds and thousands. Train shoots were greatly simplified and idealized.

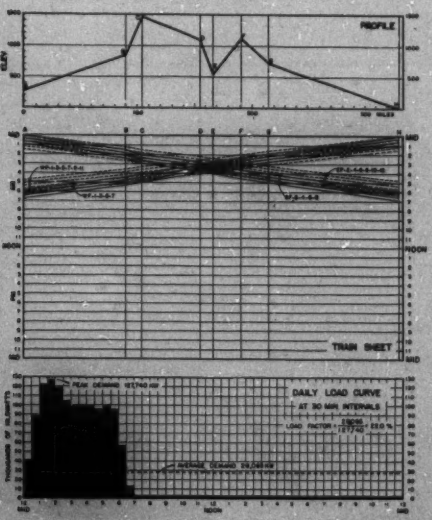

Figure 1. Profile train sheet and substation load chart for analysis basis number 1

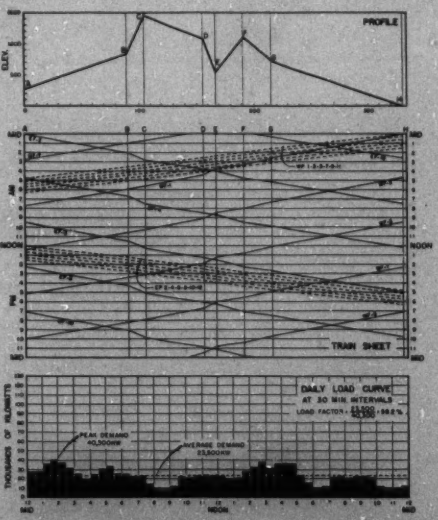

Figure 2. Profile train sheet and substation load chart for analysis basis number 2

western observations, it is believed that an electrification extending over 330 miles of profile similar to that assumed in the Evans and Kimball and Holm papers would easily handle the assumed traffic with an installation totalling not more than 50 per cent of the installed frequency changer and track-side substation capacity contemplated in these papers. Likewise it is believed that traffic of this density should produce an electrical load factor on an average monthly basis of at least 45 per cent without sacrificing any flexibility of operation that the railroad could reasonably and efficiently utilize. And, as pointed out earlier, this could be accomplished with a saving of at least 50 per cent in the required investment in motive power, a consideration that is not inconsequential.

Reference

1. Railroad Electrification Energy Conversion and Transmission Costs, **R. L. Kimball, J. G. Holm.** AIEE Transactions, volume 67, 1948, pages 219–30.

H. F. Brown (New York, New Haven and Hartford Railroad, New Haven, Conn.): In Table I of the paper Evans shows the effect on load factor by an assumed traffic diversity. Many factors control the ability to spread out railroad load. Some lines have definite daily markets to meet with their freight trains. Others, like "coal" roads, can schedule their trains to make a very uniform traffic flow. The net effect, as the table shows, is only to reduce demand charges for electric power.

Another author, J. F. N. Gaynor,[1] has pointed out the very restrictive effects of demand charges on electric power costs. It would seem equitable, if large power developments really desire the railroad base load, to work out lower demand charges which would take more into consideration the diversity between the railroads' demands and the power system peaks, allowing the railroads to have a certain proportion of the off-peak load, and at night, when normal industrial demands are at a minimum, without such restrictive demand charges, just as household water heating is encouraged by some electric utilities to build up their load factor.

The railroads are utilities, just as are power supply systems, and both being public servants should be able to work out such problems together in the interests of better public service, as well as to their mutual benefit.

Exploring further the unit costs of power presented by the author in Table I of the paper, and plotting on the power cost chart (Figure 1 of the paper) the average cost per kilowatt-hour for the load factors shown, for both diversities and for both types of electrification, it will be found that the unit costs of d-c power fall on a curve about 1 mil above curve *B* (Large Steam Power System Rate), and the a-c unit costs fall on a curve almost coincidental with curve *B*, except for load factors of 30 per cent or below, where they drop slightly below curve *B*.

This does not indicate that the "Large Water Power System Rate," has a very great effect on the proposed electrification power costs; and does seem to indicate that, in general, large hydro developments (especially those made at the expense of public funds) are not usually located where industry or transportation can use their output; or if their output can be so utilized, the hydro stations must be largely backed-up by steam stations, which determine the rates.

If the unit costs shown are due to conversion costs, then my comments on such made in connection with the paper by Kimball and Holm[2] are pertinent.

References

1. Comparative Operating Results of Steam, Diesel-Electric and Electric Motive Power on the Great Northern Railway Electrification, **Joseph F. N. Gaynor.** AIEE Transactions, volume 67, 1948, pages 231–5.

2. Railroad Electrification Energy Conversion and Transmission Costs, **R. L. Kimball, J. G. Holm.** AIEE Transactions, volume 67, 1948, pages 219–30.

J. G. Holm (Gibbs and Hill, Inc., New York, N.Y.): Evans has made a significant contribution in his paper relating the cost of power purchased by an electrified railroad to the cost of electrification and in suggesting that an electrified railroad purchasing its power can operate economically even when handling a medium volume of traffic.

Since the hypothetical railroad considered is 330 miles long, Evans naturally postu-

Table I. Estimated Train Schedules, Substation Demands, and Energy Requirements—Eastbound Movements

Section	Miles	Speed (Miles Per Hour)	Time, Hours	Unit Resistance: Grade (Pounds)	Unit Resistance: Tractive[4] (Pounds)	Unit Resistance: Total (Pounds)	Total Resistance (Pounds)	Rail: Horsepower	Rail: Kilowatts	Substation[5]: Kilowatts	Substation[5]: Kilowatt-hours
								Bracketed ( ) Quantities Indicate Regeneration			
Freight Trains[1] (Figure 1[6])											
A-B	90	60	1.5	+ 2.4	9.5	+11.9	66,640	10,650	7,950	12,000	18,000
B-C	15	30	0.5	+15.8	5.7	+21.5	120,400	9,650	7,200	11,000	5,500
C-D	51	64	0.8	+ 0.4	10.0	+10.4	58,240	9,900	7,400	11,000	8,800
D-E	12	60	0.2	−14.0	9.5	− 4.5	−25,200	(4,030)	(3,010)	(2,000)	(400)
E-F	24	40	0.6	+ 9.0	6.8	+15.8	88,480	9,450	7,050	11,000	6,600
F-G	24	60	0.4	− 3.0	9.5	+ 6.5	36,400	5,820	4,350	7,000	2,800
G-H	114	60	1.9	+ 0.8	9.5	+10.3	57,680	9,400	7,000	11,000	20,900
Totals	330	55	5.9								62,200
Passenger Trains[3] (Figures 1 and 2)											
A-B	90	75	1.2	+ 2.4	10.3	+12.7	13,300	2,640	1,970	2,800	3,360
B-C	15	50	0.3	+15.8	7.4	+23.2	24,400	3,250	2,420	3,600	980
C-D	51	73	0.7	+ 0.4	10.0	+10.4	11,000	2,140	1,600	2,400	1,680
D-E	12	60	0.2	−14.0	8.5	− 5.5	−5,800	(930)	(695)	(500)	(100)
E-F	24	60	0.4	+ 9.0	8.5	+17.5	18,400	2,940	2,200	3,300	1,420
F-G	24	60	0.4	− 3.0	8.5	+ 5.5	5,800	930	695	1,000	400
G-H	114	71	1.6	+ 0.8	9.8	+10.6	11,100	2,100	1,570	2,400	3,840
Totals	330	69	4.8								11,580
Freight Trains[2] (Figure 2)											
A-B	90	45	2.0	+ 2.4	7.4	+ 9.8	45,200	5,420	4,040	6,000	12,000
B-C	15	19	0.8	+15.8	4.8	+20.6	95,000	4,750	3,540	5,500	4,400
C-D	51	51	1.0	+ 0.4	8.2	+ 8.6	39,600	5,400	4,030	6,000	6,000
D-E	12	60	0.2	−14.0	9.5	− 4.5	−20,700	(3,320)	(2,480)	(1,500)	(300)
E-F	24	30	0.8	+ 9.0	5.7	+14.7	67,800	5,420	4,040	6,000	4,800
F-G	24	60	0.4	− 3.0	9.5	+ 6.5	30,000	4,800	3,580	5,500	2,200
G-H	114	50	2.3	+ 0.8	8.1	+ 8.9	41,000	5,460	4,080	6,000	13,800
Totals	330	44	7.5								42,900

[1] 4,880 tons trail + two 360-ton locomotives = 5,600 gross tons.

[2] 655 gross tons trail + one 360-ton locomotive + one 35-ton heater car = 1,050 gross tons.

[3] 4,250 gross tons trail + one 360-ton locomotive = 4,610 gross tons.

[4] Davis' formulae for 50-ton 4-axle freight and 60-ton 6-axle passenger cars. Locomotive resistance same basis as cars hauled.

[5] At 3-phase 60-cycle delivery points.

[6] Figures 1 and 2 refer to figures in discussion.

lated that the railroad purchases power from three separate public utilities. As a result, the railroad has three separate power contracts and must pay for the purchased power at varying rates. Moreover, the power schedules chosen indicate that at low-load factors the rates are very unfavorable. It is reasonable to assume that such a railroad would have to deal with several sources of power supply at varying rates. However, the railroad would strive for uniformity in power contracts, if not for entirely uniform power rates, and has powerful arguments to use in these negotiations. Assuming that the power companies are not directly interconnected with transmission lines, in supplying power to the railroad they may be interchanging power to a certain extent through the railroad circuits. This situation might lead to an agreement between them resulting in more uniform rates to the railroad. It should be borne in mind that an electric railroad is a large and steady power customer, and there is always the possibility that it might erect its own power plant. These factors would tend to cause the public utilities involved to arrange between themselves for the interflow of power supplied to the railroad. Highly advantageous operating conditions could be obtained for the power companies, and rates might be secured for the railroad that are more uniform and more economical than those assumed by Evans.

If my understanding of the author's figures is correct, Evans calculated his load factors for the sum of the maximum peaks at each station. These, however, do not actually occur simultaneously. If the calculation of load factors was made on this basis, it may be favorable to the public utility, but results in less favorable rates to the railroad.

The method of determining the demand charges also has a bearing on the final cost per kilowatt-hour of purchased power to the railroad. It is not quite clear in Evans' paper whether the demand charge is taken for the sum of individual station demands for a maximum month, or for the average monthly simultaneous demand. It seems to me that the diversity factor 2 should be applicable to the average demand rather than to the maximum demand, otherwise the calculation of total demand charges might be high.

The question of power requirement for tonnage moved is important. Evans estimated it to be 37,000 kilowatt-hours per million gross ton-miles. Although this figure is quite representative, it must be pointed out that it varies greatly on existing railroads. On one railroad, for instance, the figure is considerably greater and on another railroad in the same area it is smaller than the figure assumed in the paper. A higher power requirement should unquestionably result in lower power rates.

Finally, one more point is of importance. Evans' objective was to calculate the cost of power as delivered at the locomotive. He considered separately the d-c and the a-c electrifications. The Kimball and Holm paper, however, considers direct current and two types of a-c locomotives: one operating at an 85 per cent lagging power factor and the other at 95 per cent leading power factor. The latter locomotive relieving the power-supply system of reactive load would have certain beneficial effects which might result in somewhat more favorable power rates.

When all the factors discussed are properly considered, the economy of an a-c or a d-c electrified railroad handling medium volume of traffic may show up to even greater advantage than indicated in Evans' interesting paper.

Llewellyn Evans: To answer Holm's and Brown's questions in regard to method of arriving at the demands at each station; the average billing demand used was 80 per cent of the demand for the highest month. The maximum demand was arrived at by spacing the trains in fleets (as proposed in the Kimball and Holm paper) with the trains 18 miles apart and followed by trains 40 miles apart. The load arriving at the meter of any station was a composite of the fully loaded locomotives on either side of the station divided inversely proportional to the miles between the train and the stations. The peak was investigated for all parts of the schedule and an allowance made for possible errors of operation. These demands resulted in figures shown in Table II of the paper, and figures shown in Table III are the result of dividing the demand for the average month by two.

Answering T. M. C. Martin's discussion, I have figured the bills for his time table on the same schedule but without the tolerances or diversities and the results have

Table II. Estimated Train Schedules, Substation Demands, and Energy Requirements—West Bound Movements

Section	Miles	Speed (Miles Per Hour)	Time (Hours)	Unit Resistance: Grade (Pounds)	Unit Resistance: Tractive[1] (Pounds)	Unit Resistance: Total (Pounds)	Total Resistance (Pounds)	Rail: Horsepower (Bracketed ( ) Quantities Indicate Regeneration)	Rail: Kilowatts	Substation[2]: Kilowatts	Substation[2]: Kilowatt-hour
Freight Trains[1] (Figure 1)											
H-G	114	60	1.9	+ 2.4	9.5	+11.9	66,640	10,650	7,950	12,000	22,800
G-F	24	48	0.5	+ 6.2	7.8	+14.0	78,400	10,000	7,460	11,000	5,500
F-E	24	60	0.4	− 5.8	9.5	+ 3.7	20,720	3,300	2,460	4,000	1,600
E-D	12	30	0.4	+17.2	5.7	+22.9	128,240	10,250	7,650	12,000	4,800
D-C	51	57	0.9	+ 2.8	9.1	+11.9	66,640	10,000	7,460	11,000	9,900
C-B	15	50	0.3	−12.6	8.1	− 4.5	−25,200	(3,360)	(2,500)	(2,000)	(600)
B-A	90	60	1.5	+ 0.8	9.5	+10.3	57,680	9,400	7,000	11,000	16,500
Totals	330	56	5.9								60,500
Passenger Trains[1] (Figures 1 and 2)											
H-G	114	71	1.6	+ 2.4	9.8	+12.2	12,800	2,420	1,800	2,700	4,320
G-F	24	60	0.4	+ 6.2	8.5	+14.7	15,400	2,460	1,840	2,800	1,120
F-E	24	60	0.4	− 5.8	8.5	+ 2.7	2,840	455	340	500	200
E-D	12	60	0.2	+17.2	8.5	+25.7	27,000	4,300	3,200	4,800	960
D-C	51	73	0.7	+ 2.8	10.0	+12.8	13,400	2,600	1,940	2,900	2,030
C-B	15	50	0.3	−12.6	7.4	− 6.2	−5,450	(730)	(550)	(400)	(120)
B-A	90	75	1.2	+ 0.8	10.3	+11.1	11,700	2,340	1,750	2,600	3,120
Totals	330	69	4.8								11,630
Freight Trains[3] (Figure 2)											
H-G	114	44	2.6	+ 2.4	7.3	+ 9.7	44,700	5,240	3,910	6,000	15,600
G-F	24	34	0.7	+ 6.2	6.2	+12.4	57,000	5,200	3,880	6,000	4,200
F-E	24	60	0.4	− 5.8	9.5	+ 3.7	17,000	2,720	2,030	3,000	1,200
E-D	12	17	0.7	+17.2	4.7	+21.9	101,000	4,600	3,430	5,000	3,500
D-C	51	42	1.2	+ 2.8	7.1	+ 9.9	45,600	5,180	3,860	6,000	7,200
C-B	15	50	0.3	−12.6	8.1	− 4.5	−20,700	(2,760)	(2,060)	(1,500)	(450)
B-A	90	50	1.8	+ 0.8	8.1	+ 8.9	41,000	5,470	4,080	6,000	10,800
Totals	330	43	7.7								42,050

[1] 4,880 gross tons trail + two 360-ton locomotives = 5,600 gross tons.

[2] 655 gross tons trail + one 360-ton locomotive + one 35-ton heater car = 1,050 gross tons.

[3] 4,250 gross tons trail + one 360-ton locomotive = 4,610 gross tons.

[4] Davis' formulae for 50-ton 4-axle freight and 60-ton 6-axle passenger cars. Locomotive resistance same basis as cars hauled.

[5] At 3-phase 60-cycle delivery points.

Table III. Estimated Power Bills for A-C and D-C 330-Mile Electrified Railroad at 1940 Traffic Level, 100 Per Cent According to the Schedules in the Discussion by T. M. C. Martin

Stations *A*, *C*, and *a*, *b*, *c* Are Supplied by a Hydro Network
Stations *E* and *d*, *e*, by a Small Steam Network
Stations *F* and *f* by a Large Steam Network
Demands Are for an Average Month, 80 Per Cent of Maximum Month

Station	Kw Demand	1,000 Kwhr Per Month	Demand Charge	Energy Charge	Total Charges	Cents Per Kwhr	Per Cent Load Factor
A	14,250	3,121	12,925	8,262	21,187	0.68	30.
C	13,610	6,260	12,349	16,110	28,459	0.45	68.3
E	18,750	5,450	23,437	43,600	67,037	1.23	40.
F	12,800	2,333	12,800	16,331	29,131	1.24	25.2
	59,410	17,164	61,511	84,303	145,814		
12 months		205,968	738,132	1,011,636	1,749,768	0.84	40.8
	40,300 kw (Simultaneous)						60.

totaled 4 per cent cheaper than those of Table III, so that the method of arriving at probable power costs are fairly close to those arrived at by the more complete scheme of making a schedule and figuring the load for each grade and stretch of track.

In assuming that the railroad would buy from the power companies along its route, we have been realistic. An examination will show that the plan of distribution and transmission proposed by Kimball and Holm has a tendency to minimize any extra cost due to metering at four separate stations because the distance between stations, especially in the a-c case, is great enough to allow for the effect of diversity between trains and the voltage is high enough to permit shifting the load from any one station to the adjacent station. In fact, the railroad operator could fix an automatic limit to the demand at each station and allow any excess to spill over to adjacent stations. As a clarification of the method of figuring the power bills, I am including a twelve month's computation (Table IV of the discussion). This table contains the month by month bills for station *C* for 157 per cent traffic on the a-c system, the average month of which is shown on Table II of the paper.

To answer Holm's request for power costs for 95 per cent leading power factor locomotives, I have prepared Table III from Martin's discussion of my paper from the data in Tables I and II and Figure 2, of his paper. Inasmuch as Martin was using motor-generator set locomotives, the power requirements are of the same order.

H. F. Brown has properly raised the question of mutually negotiated contracts between the railroads utility and power utility. The schedule used in Martin's discussion shows that the time of peak on the service stations were:

E	2:00 a.m.	18,745
C	3:00 a.m.	13,614
A	4:45 a.m.	14,250
F	5:00 p.m.	12,810

A load of this kind that entirely escapes the utilities important peaks could have earned demand charges lower than those in the published schedules used in my calculations. Philip Torchio in his paper "Railway Power Supply,"[1] has covered the matter of contracts for railroad supply and I avoided duplicating this work. In a verbal discussion, H. F. Brown raised the question of the power utilities furnishing the converter stations. In special instances this also might be negotiated to the advantage of both utilities because:

(1) The motor of the frequency changer can often be used to supply condenser capacity.

(2) There are often supply points on utility systems where the 13-kv level can be used directly to supply frequency changers and save the cost of one set of transformers provided for in the Kimball and Holm estimate.

It is true that most of the large water power system rates are available at a distance from large industrial and transportation centers but there are numerous instances where railroads of intermediate traffic (about 4,000 miles of them) such as is under discussion traverse the area where such power is available at least for part of the road distance as supposed in this paper.

The detailed schedules and computations provided in Martin's discussion closely follow the probable operation of the section of line selected for study. Accordingly, I have in Table III of the discussion computed the power costs in such a fashion that they are directly comparable with Table III of the paper. The demands and consumption of the various track sections listed in Martin's Tables I and II of the discussion have been shifted to stations *A*, *C*, *E*, and *F* of the original paper. The demands were arrived at by following the schedule of Figure 2 in the Martin discussion paper. The same method (as in Table III) of proportioning the load to each station inversely as the distance from the train to the station was used. No tolerance amounts were added and no diversity applied because the method used by Martin for computing the power requirements are actually fitted to a schedule and grade. The computation of power costs show a substantial improvement in the load factor based on simultaneous demand but only 4 per cent saving in total cost. Probably in actual operation the railroad could operate to still better advantage by shifting part of the demand from station *E* to the stations on either side where the power costs are lower. As it stands the theoretical saving is not as great as might be expected largely because (1) of billing from four separate stations instead of one as supposed in Martin's discussion, (2) Martin scheduled the greatest load for station *E* which happened to have the highest rates.

In the original paper and the discussions, we have uncovered the extremes in power costs. There seems to be a very favorable opportunity for negotiating an economic power supply in a situation such as has been studied in these papers and discussions. I appreciate that the discussions have added greatly to the value of the original paper and feel repaid for having made the effort to produce a part of the symposium.

Table IV. Estimated Power Bills for Station C

157 Per Cent of 1940 Traffic With a Diversity of 2.0, A-C System, 330-Mile Electrified Railroad

	Kw Demand	1,000 Kwhr	Demand Charge	Energy Charge	Total Bill	Cents per Kwhr	Per Cent Load Factor
January	23,893	10,682	21,604	27,165	48,769	0.46	0.61
February	21,374	8,308	19,337	21,230	40,567	0.49	0.53
March	18,863	5,935	17,077	15,297	32,374	0.54	0.36
April	18,863	5,935	17,077	15,297	32,374	0.54	0.36
May	21,374	8,308	19,337	21,230	40,567	0.49	0.53
June	23,893	10,682	21,604	27,165	48,769	0.46	0.61
July	26,407	13,060	23,866	33,110	56,976	0.44	0.68
August	28,922	15,430	26,130	39,035	65,165	0.42	0.71
September	31,437	17,803	28,393	44,967	73,360	0.41	0.78
October	31,437	17,803	28,393	44,967	73,360	0.41	0.78
November	28,922	15,430	26,130	39,035	65,165	0.42	0.71
December	26,407	13,050	23,866	33,110	56,976	0.44	0.68
Total	301,792	142,436	272,814	361,608	634,422		
Average per month	25,150	11,869	22,735	30,134	52,868	0.45	0.64

Reference

1. Railway Power Supply, Philip Torchio. AIEE Transactions, volume 59, 1940, pages 550-3.

The Liquid Rheostat for Speed Control of Wound Rotor Induction Motors

G. L. McFARLAND
MEMBER AIEE

W. ALVAREZ
NONMEMBER AIEE

Scope

THE widest application of the liquid rheostat is for acceleration and speed adjustment of wound rotor induction motors. Modern wind tunnel drives, centrifugal pump and fan drives and steel mill roll drives are among those applications depending upon the liquid rheostat for speed control.

Although liquid rheostats have been applied for many years, there have been surprisingly few technical articles written describing the basic design and features of these devices. This paper has been written to acquaint the reader with the basic design of modern liquid rheostats and the basic data on types of loads and control systems.

Basic Design

Speed control systems using the liquid rheostat are noteworthy for their stepless speed control, minimum maintenance and maximum dependability. Most applications in recent years have been installations for motors ranging from 1,000 to 30,000 horsepower.

Figure 1 shows a typical rheostat design for speed adjustment of a 29,000 horsepower 3-phase wound rotor induction motor for a wind tunnel drive. This particular rheostat is approximately 18 feet high, 12 feet wide, and 7½ feet deep. Figure 2 is a rear view, showing the three separate tanks, one for each phase. The motor secondary cables are bolted to the terminal strips below each tank.

Figure 3 shows a liquid rheostat for a 9,000 horsepower motor. This particular rheostat is 15 feet high, 8 feet wide, and 5 feet deep. Electrodes for all three phases are mounted in a single tank.

Basically, the liquid rheostat consists of metal electrodes in a tank of electrolyte, usually a solution of a few per cent of sodium carbonate by weight in clean water. The sodium carbonate used should not contain more than 1/2 per cent of sodium chloride or sodium sulfate or more than 1 per cent of sodium bicarbonate if maximum electrode life is to be expected.

Current flows between the electrodes through the electrolyte, the resistance between electrodes being approximately proportional to the distance between them.

To insulate the stationary electrodes from the steel tank and from each other, an insulating cell is used around each pair of electrodes. Cell construction is typified as open cell or closed cell construction. The closed cell design is used when a high value of resistance per phase is required at maximum electrode spacings, such as for centrifugal fan, pump, and compressor load applications. The open cell design is used where relatively low resistances per phase are required, as for constant torque loads. Smooth speed control is obtained by varying the resistance in each phase of the motor secondary, the resistance being varied by raising or lowering the movable electrode.

Figure 4 shows a schematic drawing illustrating the internal design of a liquid rheostat in which the three insulating cells are mounted in one electrolyte tank. Details of the closed cell construction are shown in Figure 5. Resilient gaskets between the insulator and the steel tank prevent leakage of the electrolyte. Inlet pipes, which introduce electrolyte beneath the lower electrode, are made from hard rubber as they must not short circuit the resistance path between the electrodes. For electrodes up to 10 inches in diameter, cells are made from a ceramic material. For larger sizes, rubber coated steel cells are used for greater strength.

Figure 6 shows the open cell design in which circulation ports near the lower electrodes allow the electrolyte to circulate up through the cell and out over the cooling coils by thermal convection. Phase insulation between cells is required to prevent excessive current leakage between the stationary electrodes.

Since the resistance of the rheostat is to be easily changed, the movable electrode assembly is connected to an operating mechanism of some kind. These mechanisms may be:

1. Pilot motor.
2. Torque motor.
3. Air cylinder.
4. Hydraulic cylinder.
5. Manual device.

The movable electrodes are partly counterbalanced with weights to minimize the power required to move them.

The power loss in the electrolyte between electrodes is instantly absorbed by the electrolyte itself, thus raising its temperature in the portion carrying current. The horsepower dissipation rating of the rheostat then depends upon the means for cooling the electrolyte.

The open cell design takes advantage of the natural convection created by differences in electrolyte temperature. Cooling coils are generally used directly in the rheostat tank itself, the heat being removed by cooling water. This usually results in the lowest rheostat cost, although it will not be a minimum size.

Where closed cells are used, the electrolyte is pumped to the bottom of the cell beneath the lower electrode to insure positive circulation. Since an electrolyte pump is used, the electrolyte is forced through a liquid-to-liquid heat exchanger

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G. L. McFarland and W. Alvarez are both with the control engineering division of the apparatus department, General Electric Company, Schenectady, N. Y.

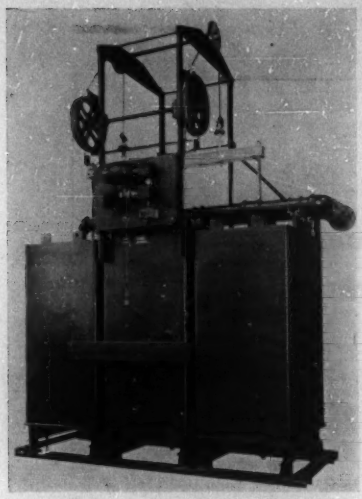


Figure 1 (left). Liquid rheostat for speed adjustment of 29,000 horsepower 3-phase wound-rotor induction motor for wind tunnel service

18 feet high, 12 feet wide, and 7½ feet deep

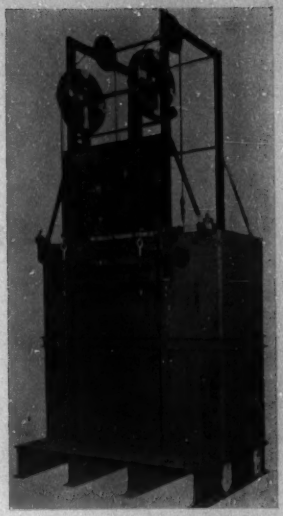


Figure 3. Liquid rheostat for a 9,000 horsepower wound-rotor induction motor

15 feet high, 8 feet wide, 5 feet deep

outside the rheostat. This results in the smallest over-all size of device and has the advantage that the external heat exchanger can be cleaned very easily when necessary.

Resistance properties of the electrolyte depend upon the concentration of sodium carbonate in the electrolyte and upon the temperature of the solution. Figure 7 shows the variation in resistance due to these two factors. From the curves, the resistance at 65 degrees centigrade is roughly 50 per cent of the resistance at 20 degrees centigrade. In other words, the electrolyte has a negative temperature coefficient of resistance. Resistance between electrodes also depends upon the current density existing at the electrode surface. (See Figure 8.) Resistance is decreased approximately 8 to 14 per cent when the current density is increased from 3 to 10 amperes per square inch of electrode area.

These resistance variations of the electrolyte do not present any problems in rheostat operation, however, as the electrodes are easily moved to the desired resistance setting.

Figure 9 shows a piping layout of a typical installation with external coolers. Since the circulating pump for the elec-

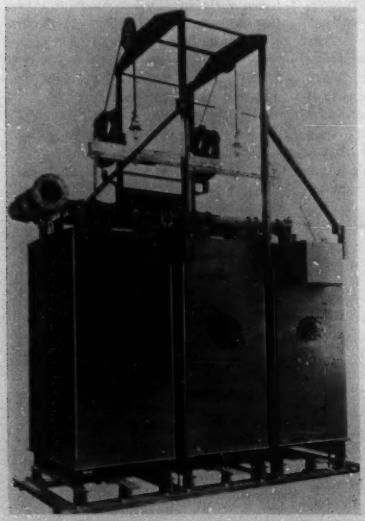


Figure 2 (left). Rear view of rheostat shown in figure 1

Terminals for motor connection beneath each tank

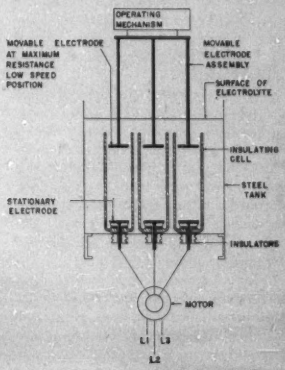


Figure 4. Liquid rheostat with closed cell construction, forced circulation of electrolyte

External heat exchanger for cooling electrolyte not shown

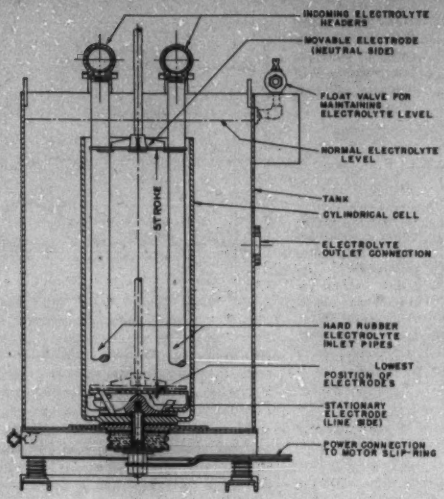


Figure 5. Cross section of closed cell design showing incoming electrolyte headers which feed electrolyte below lower electrode

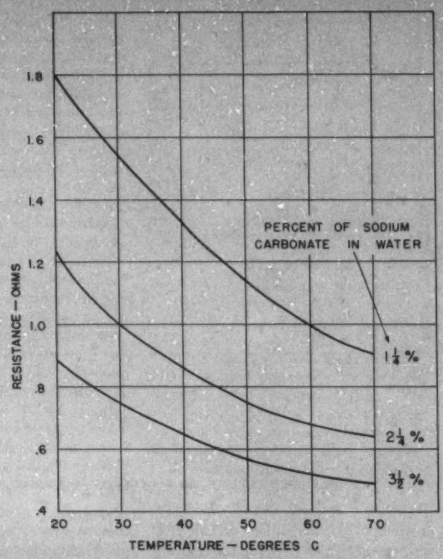


Figure 7 (above). Properties of sodium carbonate electrolyte

Variation of resistance with temperature and percentage of sodium carbonate by weight in clean water. Data taken at constant current density

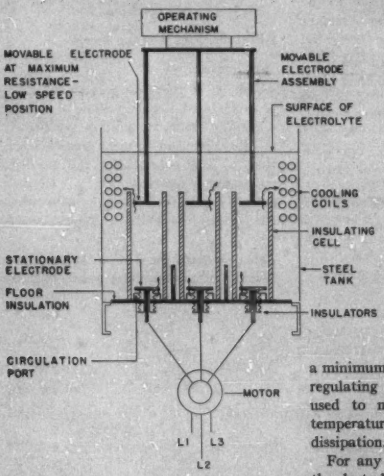


Figure 6 (left). Liquid rheostat with open cell construction

Gravity circulation of electrolyte. Cooling coils for cooling electrolyte mounted in rheostat tank

trolyte always operates with a positive suction head, no air pockets can accumulate in the piping system at any point. Sump tanks are not required for successful operation of the rheostat. The over-temperature relay provides protection against excessive electrolyte temperatures. To keep the cooling water flow to a minimum, a temperature control valve, regulating the flow of cooling water, is used to maintain constant electrolyte temperature for all values of horsepower dissipation.

For any given horsepower dissipation, the electrolyte flow depends entirely upon what difference in electrolyte temperatures can be allowed. Figure 10 shows that minimum electrolyte temperature differential is obtained by maximum electrolyte flow. The electrolyte pump must provide 11.3 gallons per minute per 100 horsepower dissipation to limit the outgoing electrolyte temperature to 65 degrees centigrade when the electrolyte inlet temperature is 40 degrees centigrade.

Makeup water is required to maintain constant electrolyte level as slight evaporation and hydrolysis occur. As in the case of the water originally used to fill the tank, makeup water should be clean and suitable for drinking purposes.

Motor Characteristics

For the sake of clarity, the convenient per unit system of expressing physical quantities is used, and the internal rotor circuit resistance of the motor is neglected. The term "rotor circuit resistance" used frequently in the discussion therefore includes only the resistance in the liquid rheostat, unless otherwise specified.

Figure 11 shows typical motor speed-torque curves: *A-B-R1-H*, *A-D-R2*, and *A-F-K-R3* for corresponding values of rotor circuit resistance, which are read directly at point *R1* (0.63), point *R2* (1.0), and point *R3* (2.0), respectively. Similarly, other motor speed-torque curves may be plotted for other desired values of rotor circuit resistance. These speed-torque curves may be assumed to be straight lines over the torque range indicated on Figure 11.

Load Characteristics

Centrifugal Pump and Fan Loads. A common type of load characteristic is that

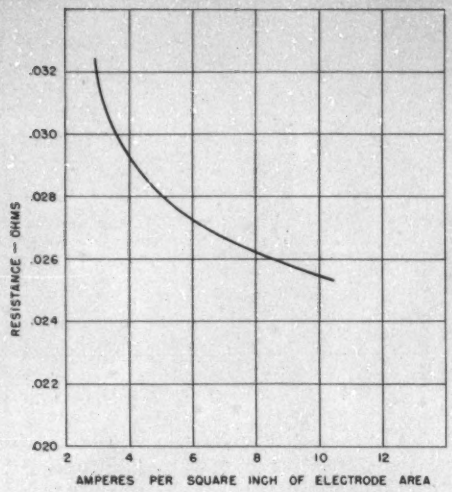


Figure 8 (left). Properties of sodium carbonate electrolyte—variation of resistance with current density

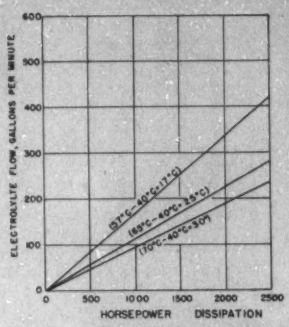


Figure 10. Horsepower dissipation and electrolyte flow requirements for various electrolyte temperature differential

provided by centrifugal fans, pumps, and compressors. This type of load, in an ideal sense, is classified by the relationship, "the load torque varies as the square of the speed," that is,

$$T = 1.0N^2$$

This load characteristic is illustrated in Figure 11 by curve *O-B-D-F-M*.

To illustrate the use of Figure 11, assume that the motor is at standstill and that the rotor circuit resistance is selected. At $R=2.0$, read at point *R3*. At the instant rated voltage is applied to the motor, the stalled motor torque is read at point *K*, $T=0.5$ at $N=0$. The initial current at standstill is also read at point *K*, $I=0.5$. The motor accelerates from standstill at point *K* along curve *K-F* to point *F*, where the speed reaches a steady-state value $N=0.5$ with $T=0.25$ and $I=0.25$. Reading vertically up from point *F* to point *G*, the slip is observed to be, $S=0.5$.

Similarly, selecting $R=1.0$ at point *R2*, the stalled motor torque is read at point *R2* ($T=1.0$, $I=1.0$). The motor accelerates from point *R2* along curve *R2-D* to point *D*, where the final operating speed is $N=0.62$, with $S=0.38$, $T=0.38$, and $I=0.38$.

Where $T=1.0N^2$, the rotor circuit resistance per phase, R, required to give any steady-state operating speed, N, may be derived as follows: From Figure 11, at point *F*:

$$\tan\varphi = \frac{T_F}{1.0-N_F} = \frac{N_F^2}{1-N_F} \quad \text{since } T_F = 1.0N_F^2 \qquad (1)$$

$$\text{Also, from Figure 11 } \tan\varphi = \frac{1.0}{R_3} \qquad (2)$$

where R_3 is the resistance value intercepted by motor characteristic *A-F-K-R3*.

Solving for R_3 in equation 2, substituting the expression for $\tan\varphi$ given by equation 1, and generalizing the resulting expression, we obtain,

$$R = \frac{S}{N^2} \text{ where } S = 1-N \qquad (3)$$

This expression for R is plotted in Figure 12 and it shows that large values of resist-

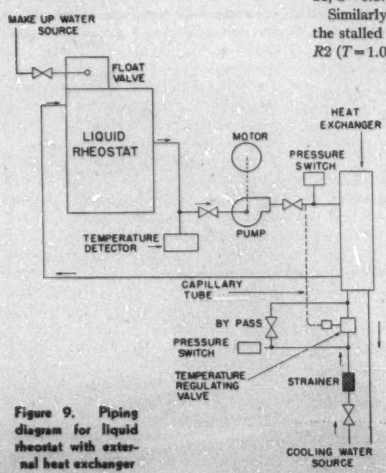


Figure 9. Piping diagram for liquid rheostat with external heat exchanger

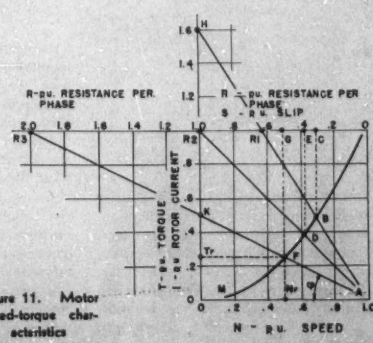


Figure 11. Motor speed-torque characteristics

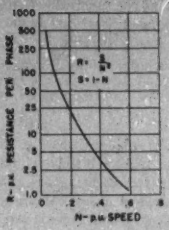


Figure 12. Resistance versus speed for a load torque $T=1.0N^2$

ance are required to obtain low operating speeds. For example, referring to Figure 12, to obtain operation at $N=0.1$ or 10 per cent speed, $R=90$. If the per unit value of R is, say, 1 ohm, then 90 ohms per phase would be required to operate at 0.1 per unit speed.

Therefore, Figure 12 suggests that there are practical limits to the amount of speed reduction which can be provided with centrifugal fan, pump, or compressor loads. These limits are derived from various factors which depend on the specific application, and a comprehensive discussion on this subject is beyond the scope of this article. However, these factors are listed:

1. Control sensitivity.
2. System stability.
3. Rating of the motor.
4. Maximum practical stroke of the electrodes.
5. The load characteristic.

Where $T=1.0N^2$, the power dissipated in the rotor circuit resistance may be derived as follows:

$$P_R=I^2R$$

Assuming the motor operates at a rated voltage, $T=I=1.0N^2$ and using equation 3, we have,

$$P_R=SN^2, \text{ where } S=1-N \qquad (4)$$

This relationship is plotted in Figure 13 and represents the power that has to be absorbed by the liquid rheostat at any

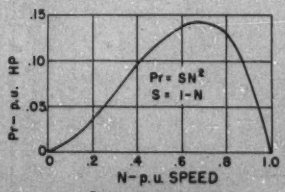


Figure 13. Rotor power dissipation versus speed for a load torque $T=1.0N^2$

steady-state operating speed. Obviously, this power dissipation affects the over-all efficiency of the motor drive, but its effect cannot be considered unfavorable or prohibitive. As a matter of fact, the curve shown in Figure 13 is inherent also for magnetic and hydraulic couplings, a factor which makes all of these methods of speed adjustment comparable in over-all efficiency.

Constant Torque Loads. Another common type of load characteristic is "the constant torque load." Figure 14 shows, for a constant torque load $T=1.0$ that it is possible to provide speed adjustment over a wide range with relatively low values of external rotor circuit resistance. However, this is achieved at the expense of high power dissipation in the rotor circuit. For example, to obtain 0.5 speed reduction, Figure 14 indicates that $R=0.5$. From Figure 15, the corresponding value of power dissipation in the rotor circuit is $P_R=0.5$ of rated motor horsepower. The significance of this example can be expressed as follows:

1. The over-all efficiency of the motor drive is low at low operating speeds.
2. The dotted curve shown in Figure 15 is taken from Figure 13 (for a load $T=1.0N^2$) and is drawn for comparative purposes.

It illustrates that for the same speed reduction, the liquid rheostat installation for a constant torque load must dissipate more power and hence it is a more expensive installation.

Because of the above economic limitations, the operating speed range is usually small for a constant torque load. In such applications, the liquid rheostat is used more as a load-regulating element. This type of application will be discussed later.

Control Systems

Manual Control System. One of the simpler methods of speed adjustment is illustrated in Figure 16. The electrodes are positioned to obtain any desired operating speed by manual control for the pilot motor on the rheostat. Once positioned, the electrodes remain stationary.

This scheme inherently provides smooth or "stepless" speed adjustment. The accuracy with which speed is maintained at fixed electrode spacing depends on constancy of the load torque and electrolyte temperature.

The temperature regulating valve which is usually provided in the electrolyte circulating system serves to maintain the electrolyte temperature within reasonable limits. However, the thermal storage in the volume of electrolyte and the fixed

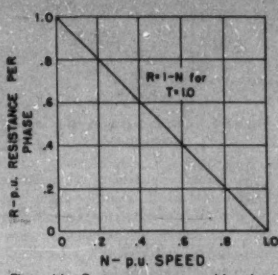


Figure 14. Resistance versus speed for a load torque $T=1.0$

pumping capacity result in a time delay for the electrolyte temperature to respond and reach a steady operating value. This thermal lag is reflected as a drift in speed after each change in speed setting and particularly after acceleration from rest.

To appraise the effect of electrolyte temperature fluctuations, consider a 20 per cent increase in external rotor circuit resistance at 50 per cent speed and again at 90 per cent speed, for a motor driving a load where $T=1.0N^2$. Refer to Figure 12.

1. At 50 per cent speed, $R=2.0$ initially and it changes to $R=1.2\times2.0=2.4$. From Figure 12

$$2.4=\frac{(1-N)}{N^2} \text{ or } N=0.47$$

The speed changes approximately 6 per cent for a 20 per cent change in rotor circuit resistance.

2. At 90 per cent speed, $R=0.124$ initially and it changes to $R=1.2\times0.124=0.15$.

$$0.15=\frac{(1-N)}{N^2} \text{ or } N=0.88$$

The speed changes approximately 2.3 per cent for a 20 per cent change in rotor circuit resistance.

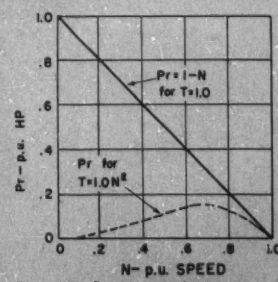


Figure 15. Rotor power dissipation versus speed for a load torque $T=1.0$

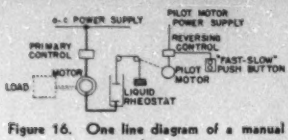


Figure 16. One line diagram of a manual control system

For specific applications, the performance of the manual control system can be appraised in somewhat the manner described above. The results may show that this type of system is satisfactory and meets the particular operating requirements.

Speed Regulating Control System. A more refined scheme of speed adjustment is illustrated in Figure 17. The pilot motor on the liquid rheostat is energized by an amplidyne. The amplidyne, in turn, responds from the control unit, which compares the speed adjustment setting of the speed-adjusting rheostat with the motor speed, as detected by the tachometer generator. Any divergence from the desired setting results in smooth, automatic positioning of the electrodes to correct and maintain the speed within precise limits. Current limit action is usually provided to prevent the motor current from exceeding a preset value during acceleration.

The speed regulating control system has several features and advantages as follows:

1. It is a variable-response system, that is, when the motor speed diverges from the desired setting, the magnitude of the correction signal is directly proportional to the value of the divergence. Thus, for a large divergence in speed, the electrodes move rapidly to correct the speed. Conversely, for a small divergence, the electrodes move slowly to correct speed. From an operating standpoint, the variable-response system results in fast response with stable operation.

2. It provides current-limit acceleration. Figure 18 illustrates a control motor speed-torque curve *A-B-C-D-E* for acceleration from standstill to the preset value, $N=0.9$ speed. Figure 18 assumes a load where $T=1.0\ N^2$ and a maximum liquid resistance per phase, $R=2.0$, which gives a stalled motor torque $T=0.5$ at point *A*. On energizing the motor, the current-limit control positions the electrodes to increase the motor current smoothly along curve *A-B*. From point *B* to point *C* speed control automatically becomes effective and stabilizes the speed at point *D*, the preselected value.

3. It virtually eliminates fluctuations in speed otherwise caused by electrolyte temperature drifts. This means that the speed at any setting is reached quickly and is thereafter maintained within close limits. Envelope *G-H-K-P-G* of Figure 18 can be designated as the control range envelope. For the speed regulating control system under consideration, the control range envelope has particular significance. It means that for any normal speed setting, the load can change between the limits established by the envelope, and the steady-state speed setting will be maintained closely and without overloading the system.

4. Referring again to Figure 18, the envelope *G-H-K-P-G* is established by the following factors:

a. The Heat Exchanger Capacity. Segment *G-H* gives values of torque versus speed for constant power dissipation in the rotor circuit. The heat exchanger capacity is arbitrarily assumed to be $Pr=0.148$ or 14.8 per cent of rated horsepower (refer to Figure 13).

b. The continuous rating of the motor segment *H-K* is for rated motor torque.

c. The Internal Rotor Circuit Resistance and the Minimum Resistance of the Liquid Rheostat (electrodes down). These resistances determine the slope of segment *K-P* by the relationship: the minimum motor slip (at point *k*) equals the sum of the per unit values of the internal rotor resistance of the motor and the minimum resistance in the liquid rheostat.

d. The Maximum Resistance of the Liquid Rheostat (electrodes up). Segment *P-G* arbitrarily is for $R=2.0$.

For example, in Figure 18 the speed setting assumed gives the controlled motor torque characteristic *C-D-E*, the slope of which is established by the sensitivity of the regulating control, and, in an ideal sense, can be assumed to be almost vertical. The load torque can change between a value $T=1.0$ (at the intersection of curves *C-D-E* and *H-K*), and the value of torque corresponding to point *E*. The final steady-state operating speed will lie on curve *C-D-E*. The foregoing can be extended for any other speed setting within the control range envelope.

It is necessary to qualify the nature of the load changes considered in the preceding discussion. Although the steady-state operating speed reached after a load change will lie on the controlled motor characteristic such as *C-D-E* of Figure 18, it is apparent that the speed will change during transient conditions, that is, during the time the load is changing.

Changes in load may be classified as:

(1) Gradual
(2) Rapid
(3) Impact

A gradual load change is one which occurs at a rate which is within the response of the control system, that is, at a rate which the control system can follow with permissible transient speed variations. The rapid load change or the impact load change impose limitations which require special measures. The load regulating control system which will now be discussed can be adapted for certain applications which involve rapid or impact load changes.

Load Regulating Control System. This scheme is illustrated in Figure 19 and is usually applied to maintain constant input to the motor or to prevent the load on the motor from exceeding a selected value.

The torque motor on the liquid rheostat is energized by an amplidyne. The amplidyne in turn, responds from the control unit, which compares the load adjustment setting of the load control rheostat with the primary current. Variations from the desired setting cause automatic positioning of the electrodes to vary the speed to maintain essentially constant input to the motor. The electrode operating mechanism is designed to make possible very rapid movement of the electrodes.

The load regulating control system can be adapted as follows:

1. To regulate and maintain operation at essentially constant torque.

2. To operate as a current-limiting or "cushioning" control under rapid or impact load changes of short duration. The drive usually includes a heavy flywheel, and the drive motor is intended to operate at essentially constant speed. For normal loads

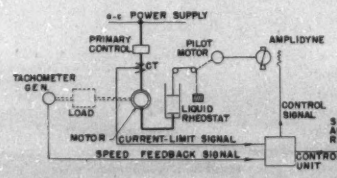


Figure 17 (left). One line diagram of a speed regulating control system

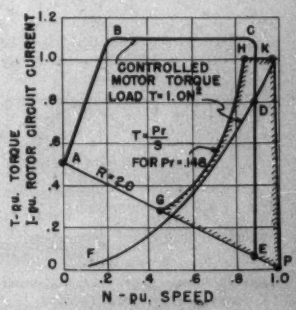


Figure 18. A controlled motor characteristic and the control range envelope

the electrodes are in the "down" or maximum speed position. When a heavy impact load occurs, the sudden increase in motor current causes the electrodes to operate very quickly and insert resistance in the rotor circuit. This tends to decrease the motor speed. However, the flywheel minimizes the speed change by giving up some of its kinetic energy. The net result is that the power input to the motor is held within the load setting. When the impact load disappears, the electrodes automatically return to the maximum speed position.

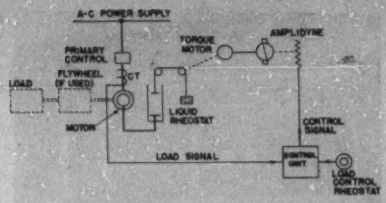


Figure 19. One line diagram of a load regulating system

Other Control Systems

The liquid rheostat, as a system element, in conjunction with the proper control, can be applied in various other ways. In some cases, a pneumatic or hydraulic cylinder can be used to operate the electrode mechanism directly. The cylinder, in turn, is positioned from a regulating control sensitive to any desired quantity such as:

1. Temperature.
2. Flow.
3. Level.
4. Pressure.

The liquid rheostat is particularly suited for control of larger motors, where other types of control cannot be used because of rating limitations. For the smaller motors the liquid rheostat is on a more competitive basis with other types of control. However, on these smaller sizes, an integral assembly comprising liquid rheostat, heat exchanger, circulating pump, and other associated devices, together with the features of stepless and precise control, will doubtless lead to wider use in the future of the liquid rheostat for control of smaller motors.

Discussion

W. Schaelchlin (Westinghouse Electric Corporation, Buffalo, N. Y.): The paper presented by McFarland and Alvarez gives a very good conception of the performance and application of the liquid rheostat. They emphasize its inherent simplicity of design and large capacity to store and dissipate energy which offer a decided advantage over other methods of speed control.

In designing its new line of liquid rheostats, the Westinghouse Electric Corporation has endeavored to "streamline" their appearance and to provide a completely closed system both for the electrolyte and for the cooling water. Another desirable improvement, particularly for high voltage applications, is obtained in the special arrangement of the stationary electrodes. With this design there is no danger whatsoever of a possible insulation failure, because each phase tank is made of insulation so that a maximum insulation is obtained when the voltage between the electrodes is highest.

Figure 1 of the discussion illustrates a cubicle for a complete secondary control with liquid rheostat built to control the speed of a 750 horsepower induced draft fan motor. It is completely enclosed and its appearance can be made to match that of the primary control having standard cubicle dimensions. A rear view is shown in Figure 2 of the discussion which illustrates the arrangement of the three rheostat tanks permitting a large speed reduction. The operating mechanism as well as the usual

Figure 1 (left). Secondary control complete with liquid rheostat for 750 horsepower induced draft fan motor

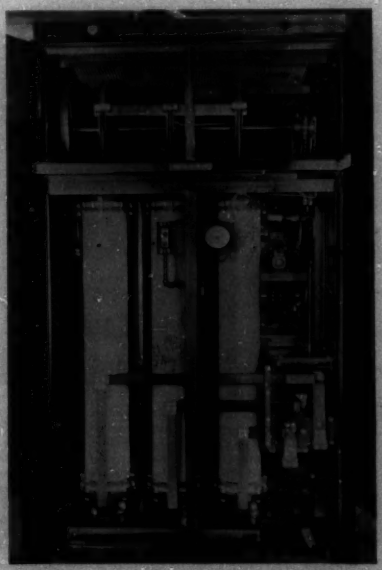


Figure 2 (right). Rear view of secondary control cubicle showing liquid rheostat

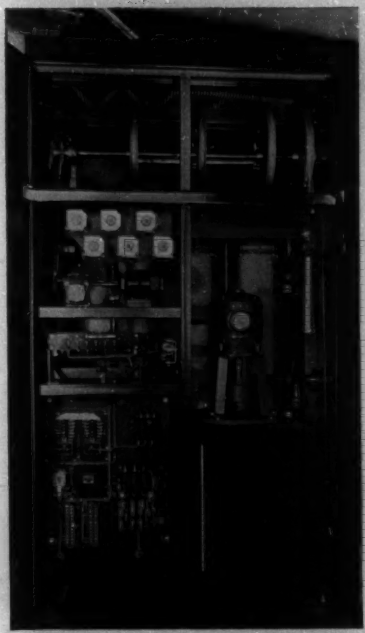


Figure 3 (left). Front view of secondary control cubicle showing operating mechanism and sump tank with pump

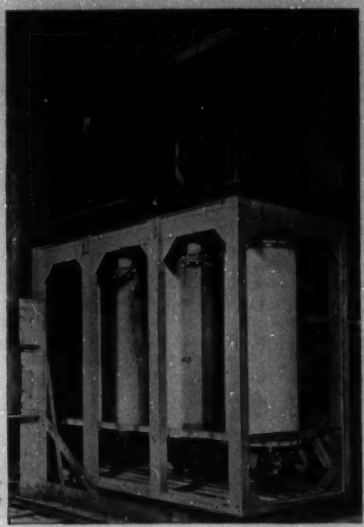
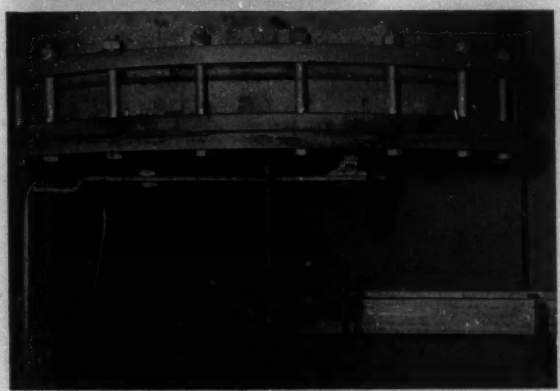


Figure 4 (right). Liquid rheostat for 10,000 horsepower wind tunnel motor

Figure 5 (below). Stationary electrode arrangement for a 25,000 horsepower liquid rheostat



control gear are mounted in the front of the cabinet which permits ready accessibility of all parts, as shown in Figure 3 of the discussion. Attention is also called to the sump tank and the electrolyte circulating pump located at the lower right side. This pump is completely submerged in the electrolyte so that any possible leaking of the glands is of no consequence and very little maintenance is therefore required to maintain a clean looking control equipment.

Figure 4 of the discussion shows a liquid rheostat for a 10,000 horsepower wind tunnel motor. The upper part consists of the cubicle with the necessary control mechanism as well as the headers for both the cooling water and the electrolyte. The cubicle itself is mounted level with the floor for easy accessibility while the three insulated tanks extend below the structure into the basement.

Considerable attention has been given to the mounting of the stationary electrodes clamped to the lower end of the insulated rheostat tank. By using a bevelled ring and groove arrangement, as illustrated in Figure 5 of the discussion, it was possible to obtain a simple and reliable joint that permits ready removal of the electrode for inspection purposes. It is interesting to note that Figure 5 illustrates the stationary electrode for a 25,000 horsepower liquid rheostat with an active electrode diameter of approximately 30 inches.

G. L McFarland: In the second paragraph of Schaelchlin's discussion, the Westinghouse liquid rheostat is discussed in which the statement is made concerning "a completely closed system for both the electrolyte and for the cooling water." Apparently, from Figure 2 in the discussion, the movable electrode is suspended on a flexible cable from the pulley wheels located above each cell. The writer assumes that there is a nominal clearance between the flexible cable and the cover of the cell even though it is not shown clearly in the photograph.

In order for the movable electrode to drop freely when the pulley shaft is rotated, close clearances cannot be used between the cover and the cable.

The fact, however, that the entire liquid rheostat is enclosed in a cubicle plus the fact that the cover on each cell must be fairly close-fitting should provide complete protection against dust and dirt entering the electrolyte system. This is what is meant by "a completely closed system."

In a new General Electric design of a liquid rheostat, we are enclosing the entire electrolyte system by other means which fit into our particular design. Evaporation losses in both designs are held to a minimum by such enclosures.

The "Director" for Automatic Telephone Switching Systems

ARTHUR BESSEY SMITH
FELLOW AIEE

THE TELEGRAPH was invented in 1837 by Morse. In 1876 came the invention of the telephone by Bell. By this time the switching of telegraph lines had developed into a definite system, characterized by plugs and jacks for easy connection and disconnection. Each connection lasted for a relatively long time, hours often running into days.

The first telephone switchboards were very much like the telegraph switchboards. But the telephone naturally required many connections and disconnections per hour, each connection lasting only a few minutes. This led to making the plugs and jacks smaller and more easy to handle.

It is a remarkable fact that very soon after Bell's invention the first attempts were made to do the telephone switching automatically ("to do by a machine that which formerly was done by hand"). And about 1891 the work of Almon B. Strowger got started, which developed into that of the Automatic Electric Company. It was the adoption of that system of automatic switching for the London, England, telephone exchange that led to the invention and development of the "Director," which is the subject of this paper.

The Director is a telephone switching device which stores the dialed call number and then sends out decimal impulses which guide the connection to the desired telephone office and to the called line.

When electric light and power came to London, the authorities ruled that all wires must be laid underground. The first telephone system in London, like others of that era, was entirely manual, and also was forced to put its wires underground.

In addition to slowing the growth of telephone use, this tended to fix the routes of telephone cables and to make it difficult to open new routes between offices as new offices were opened. To use tandem trunking was cheaper than to install direct trunks between all offices.

Direct trunks with straightforward dialing has much to commend it. There being no Director, there is no storage of impulses and no loss of time due to that. The subscriber pulls the call number, and when the dial gets home from the last digit, the connector has arrived at the called line. There remains only the short time to test the line, and if free, to start the ringing, which the subscriber can hear. Also, there is the psychological aspect—he has been usefully employed during the time and his wait is short.

It is tandem trunking which makes the Director necessary, as also the great desirability to be able to call a certain telephone by the same call number no matter where the calling subscriber may be located—the universal numbering plan.

Tandem trunking and the Director have been very much considered in America also. In fact, the director was installed in Havana, Cuba, and in a private automatic exchange (PAX) in Kansas City, Mo., before the London system was put into service.

There are also cases in small communities where the Director will serve. If it is desired to permit subscribers to dial over a county, without any intervening operators, many of the paths will pass through switching points, and others may use direct trunks.

The present day prospect of nationwide toll dialing in the United States of America, for which the Director is ideally suited, is one of the chief reasons for calling the Director to your attention.

Universal Numbering

Consider two small towns, not too far apart, each served by a 100-line automatic switchboard, with only 80 lines in use. Figure 1 illustrates the condition, omitting all the dotted lines. All calls in each town are made with two digits, and only connectors are needed.

If someone gets the idea of permitting the subscribers freely to dial each other between the two switchboards, the dotted lines and switches may be added. There may be incoming connectors C' whose banks are multipled onto the banks of the local connectors C. Suppose that the top level of each local connector is set aside for trunking to the other board. Thus, any subscriber in M will dial two digits to get others in his same town, but must prefix *0* to get any telephone in J. Likewise, any J subscriber must prefix *0* to get any telephone in M.

The following situation arises. To call telephone *25* in the M region, dialing from any other telephone on the M switchboard, requires the dialing of only *2* and *5*. But to dial the same telephone from any telephone connected to the J switchboard requires the user to dial *025*. This is *not* universal.

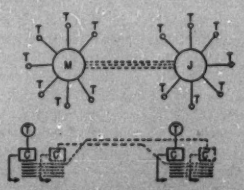


Figure 1. Two exchanges, not universal numbering

Paper 48-99, recommended by the AIEE communication committee and approved by the AIEE technical program committee for presentation at the AIEE winter general meeting, Pittsburgh, Pa., January 26–30, 1948. Manuscript submitted November 28, 1947; made available for printing December 29, 1947.

Arthur Bessey Smith is vice-president, Automatic Electric Laboratories, Inc., Chicago, Ill.

Thanks are hereby expressed to John E. Ostline (F'46) and to other members of the staff of the Automatic Electric Company for the facts and suggestions on which this paper is based.

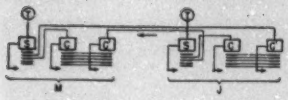


Figure 2. Two exchanges, universal numbering

Now consider Figure 2. Each of the two switchboards now has selectors *S* as well as connectors *C*. Every telephone has a 3-digit call number. As sketched, the telephones in *M* are in the 300 group and those in *J* are in the 400 group. The former telephone *25* in *M* now can be called by dialing *325* in either town. It is thus with all the telephones. This is *universal numbering*.

Tandem Trunking

Let the four offices, *E*, *J*, *K*, *M*, Figure 3, of a multioffice exchange be interconnected as usual by direct trunks. To serve a new region, office *X* is built. By reason of the local geography it would be too expensive to install direct trunk groups between the new office *X* and each of the older offices. Hence it is decided to have all calls from and to the *X* office switched at *J*. Any call from *E* or *K* or *M* would be dialed to *J*, where an additional digit would reveal that the call goes to *X* instead of to telephones connected to *J*. This is tandem trunking. If there is no Director, or its equivalent, this can not have universal numbering.

Also notice this, if a subscriber in *J* calls one in *X*, he will dial perhaps four digits. But if a subscriber in *E*, *K*, or *M* desires to call a telephone in *X*, he must first dial a digit to get to *J*, then followed by the call number for the *X* telephone. Thus the number actually dialed will depend on where the caller is located, and it is not universal numbering.

But let us place in each of the five offices a number of switching devices known as "Directors." Each of these has been so designed that it can direct or guide a call to the desired distant office no matter what route it must take. Each of these Directors is accessible to all calls coming from subscribers in that office. It is convenient to arrange it so that when a subscriber initiates a call, he will as usual get a first selector, and an idle Director will at once be associated with that first selector. The entire call number (for example, *6258*) will be dialed into the Director, and registered there. The Director has been endowed with enough electrical and mechanical judgment to know, from the first digit, *6*, that it must first send the digit *3* into the first selector to extend the line to the *J* office, to follow this by digit *6* into an incoming selector in the *J* office to cause it to rise to the group of trunks leading to the *X* office. After this, the Director will send the rest of the call number "*258*", which operates switches in the *X* office to connect the desired line. The other offices are likewise equipped with Directors having equal abilities, so that no matter where you are in the city, you can dial your friend in office *X* by the one call number, and you will get the right telephone. This is how the Director permits *universal numbering*.

This paper describes the structure of the Director, how it works, and the variety of things which it can do. It does not attempt to describe exhaustively the basic principles of tandem interoffice trunking, nor the most economical use of the equipment. But it does show the flexibility of the Director and how it can be made to serve the exchange designer and through him the public.

The London Automatic Telephone Exchange

On about the first of January 1922 the Automatic Electric Company was asked to develop an apparatus which would enable the London exchange to convert to automatic switching without installing direct trunks between all offices. Regular Strowger type numerical switches would switch the connections to their destinations.

M. L. Nelson and R. G. Richardson worked out the general plan and the circuit details and by January 6 they completed the first drawing showing practically all the details of a working Director. Though there have been numerous changes, yet many of the features of that first layout were fundamental and have remained for that reason.

In the year 1924 a Director system was built and installed in the Havana, Cuba, exchange.

In the spring of 1923 John E. Ostline went to England in charge of the further development of the circuits, and out of his work have come many improvements, some of great economic value. In 1924 they began to build the apparatus for London and in 1927 the first of it was cut into service. Since then the progress of development has been fairly steady.

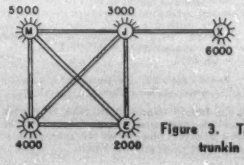


Figure 3. Tandem trunkin

First Aspects of the London Director

The situation may be illustrated by the following things which presented themselves to the designer:

1. The universal call number, such as *DAVis-8019*.
2. Tandem trunking.
3. Straight Strowger dialing of impulses, clear through to the distant office.
4. Trunk to manual offices with call indicators for the operators, at least during the transition period.

The use of the name of an office (such as Davis, Monroe, Humboldt) is a device to aid the memory of the user. It makes it more easy to remember the number from seeing it in the directory to spelling it out on the dial. The arrangement of the letters of the alphabet on the dial is well known. The first letters of the office name, which are to be dialed, are known as the office code, such as *DAV*. Dialing it results in a "decimal equivalent" such as *328*.

The coming of the Director has required the use of certain words and expressions, each to have a definite and clear meaning, so as to avoid confusion. I will illustrate by the call number *DAVis-8019*.

1. *DAV* is the office code.
2. *328* is the decimal equivalent of the office code.
3. *MNOP* or other digits form the directive. By the action of the director they are derived from the office code and they guide the call to the distant office. The number of digits in a directive depends on the route.

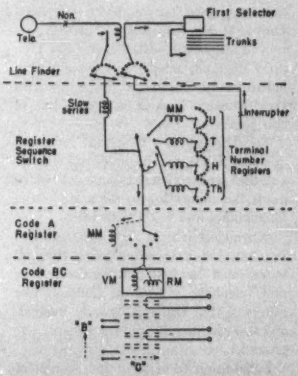
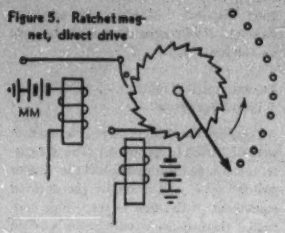


Figure 4. London plan, general, call registry

Figure 5. Ratchet magnet, direct drive



4. Directive selectors are those which are in the train leading to the distant office. Each is operated by one digit of the directive.

5. *8019* is the terminal number. As usual, the digits are called "thousands", "hundreds", "tens", and "units."

6. The thousands selector and the hundreds selector are in the distant office and handle the digits indicated by their names.

7. The connector connects to the called terminal.

The London situation led to the following first approach:

1. Carry the terminal number through unchanged (such as *8019*).

2. Transform the decimal equivalent (such as *328*) into a directive (one to six digits) to steer the call to the distant office.

3. The Director was required to store all digits dialed by the subscriber, transform the decimal equivalent of the office code into a directive, send it, and repeat the terminal number unchanged.

The Director was composed of the following subordinate devices.

1. The inward path, which guides the dialed impulses into the director.
2. The *ABC* register, on which the three decimal equivalent digits are stored.

3. The terminal number register, on which the four digits of the terminal number were stored. There was no provision for party lines.

4. A register sequence switch to guide the incoming digits, each to its appropriate register.

5. A translator, to change the *ABC* code (its decimal equivalent) into from one to six digits, called the directive.

6. An outward path from the Director to the first directive selector (in this office), from which place the path will be extended to the distant office and the called line.

7. An impulse generator, a device which generates correct impulses to be sent over the outward path.

Having seen the general plan of the Director, we are rightly interested in the various circuits used to accomplish the results. It is not practical to discuss the entire circuit assembly just now, for it is too complicated. But there are certain key circuit devices about which you are justly curious, and these we will examine now.

Many circuit details are involved, too many to cover in one paper. For some details I will use "outline symbols", combined to show the general relationships and then describe several circuit devices and their combinations to reveal the structure and action. To save space, some symbols will be used without explanation.

Call Registry

The registry of the call number is outlined in Figure 4. The telephone line comes through such nonnumerical switches (*Non*) as are used. These may be line switches or line finder switches. The trunk passes to a group of trunk relays, where two chief paths run to the banks of a group of line finders, presided over by a call distributor. The latter sends one of the line finders to seek the incoming call. Thus it is connected to an idle Director. Only the registry parts are here indicated by single line diagrams and symbols.

The incoming path from the subscriber passes through a slow relay (slow series) to the wiper of the register sequence switch. From the bank of this switch lead wires to the *ABC* code register and to the four digit registers for the terminal number (*Th*, *H*, *T*, and *U*).

The *A* digit (for example, $D=3$) operates the motor magnet *MM* of the code *A* register to contact 3 on the bank. At the same time, the slow relay has energized the motor magnet of the sequence switch, so that at the end of the impulses the slow relay falls back and de-energizes the motor magnet, whereupon the wiper moves to the next bank contact. The next two digits from the telephone operate first the vertical magnet *VM* and then the rotary magnet *RM* of the code *BC* register. This is a 2-motion Strowger switch. The second digit (for example, $A=2$) lifts the wipers to the second level, and the third digit (for example, $V=8$) rotates the same wipers to the eighth set of contacts on that level. In the meantime, the slow series relay of the sequence switch has advanced the wiper to contact 4.

It is now clear how the remaining digits for the terminal number are directed successively to the motor magnets for the thousands, hundreds, tens, and units registers.

Thus by the positions of the wipers of the *BC* register we have a recording of the office code (its decimal equivalent) and by the positions of the four wipers of the terminal register an indication of the terminal number as it appears in the telephone directory.

Magnet Ratchets

Two ratchet devices are used, each with its own properties and performances.

Figure 6. Minor switch

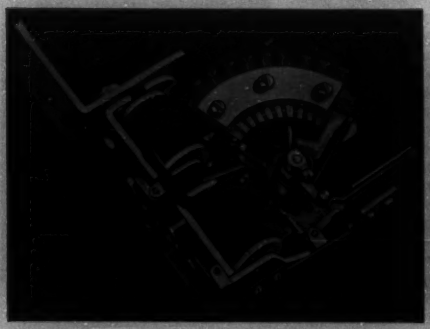


Figure 7. Strowger switch

They enable circuit designers to obtain valuable results.

Figure 5 illustrates the direct magnet drive, used in the minor switch (Figure 6) and in the vertical and rotary movements of the Strowger switch, Figure 7. When the magnet, Figure 5, is energized, the wipers move to the next contact. When the magnet releases, there is no circuit change—the ratchet is made ready for the next step. Such a ratchet action moves against a restoring force, gravity in the vertical movement of the Strowger switch and a spring in the rotary motion, as also in the minor switch. In each case there is a detent, holding the ratchet teeth from moving backward. Return to normal is by a release magnet which pulls out the detent. The minor switch is used for the register of the digits of the terminal number and the Strowger switch for the *BC* code register.

Figure 8 illustrates the spring drive, used in the rotary line switch (Figure 9). When the magnet (Figure 8) is energized, the pawl engages the next tooth but the wipers do not move. When the magnet releases, the spring drives the wipers to the next contact. This kind of switch is returned to normal by causing the motor magnet to buzz like a doorbell, so driving the wipers around the circle to the home position. This is used in the register sequence switch and elsewhere.

The Impulse Sender

The basic principle by means of which properly registered digits can be sent over the outgoing path or loop to the selectors is illustrated by Figure 10. Suppose that each of the three registers, *H*, *T*, and *U*, can be set by hand to any desired digits. I will assume *3-2-5*. Let the sequence switch *Seq.* be arranged to be moved by hand from its home contact to each of its bank contacts in sequence.

The impulse sender itself, as here abstracted, consists of a rotary switch with wipers *W* and *W'*, stepped by a motor magnet, *MM*. This is of the spring-drive type, described previously. A stop relay is connected to the wiper *W*.

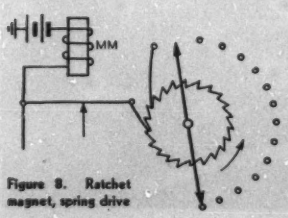


Figure 8. Ratchet magnet, spring drive

In the upper right corner of the figure is the loop leading to the first selector, and through it to other selectors in the train. Two relays close it normally, by their back contacts, relay *D*, wired to wiper *W'*, and the interrupter relay *IR*, controlled by the interrupter itself. The interrupter *I* is running steadily at the rate of ten impulses per second, and the ratio of break to entire period is correct to operate the numerical switches in the train.

Two functions are essential, to start interruptions of the loop with a full-time break (not part of one) and to stop the sending when there have been sent the number of breaks indicated by the setting of any one of the registers.

We have by hand set wiper *H* to *3*, *T* to *2*, and *U* to *5*. Then we set the sequence switch onto the *H* contact of its bank. Then we press the start key and hold it down.

If the time of pressing happens to be too late in an impulse of the interrupter, relay *IR* will fail to pull up, or make a feeble or partial response. This does no harm. But the next time *I* closes, it will operate *IR* for the full time. This energizes the motor magnet, *MM*, which pulls its armature to cause its pawl to engage a tooth. When *IR* falls back, *MM* will de-energize and drive the wipers *W* and *W'* to the next contacts. Here *W'* operates relay *D*, which pulls up and remains operated to the end of this digit, opening its contact and thereby putting the loop entirely into the control of the *IR* relay.

The next impulse from *I* operates *IR* as before, which causes *MM* to advance the wipers to contact 1. The arrival of wiper *W* at contact 1 permits the stop relay to find out if it is grounded. It is not, so two more impulses bring the wipers to contact 3, which is grounded by our setting of the *H* register and the position of the sequence switch. At once the stop relay pulls up, breaks the start wire, and ends the sending of three impulses over the loop. There were only three impulses and all of them were perfect.

In addition to stopping the impulses, the stop relay locks itself to ground on a contact of relay *D*, also connects the self-interrupter contact of the motor magnet to wiper *W'*. At once the motor magnet starts to vibrate and thus drives the wipers *W* and *W'* around the circle to the home position. Here the ground is removed from *MM* and relay *D*, the latter releases the stop relay, and ("start" being opened) the sender is ready for the next digit. Now we may move the sequence switch to the *T* contact, close the start key, and the sender will do the same process all over again, this time sending two good impulses. Moving the sequence switch to *U* will cause the same to be done for the units digit.

Translation—Decimal Equivalent Into Directive

Consider the *ABC* code registers which were discussed in Figure 4. The *A* register is a simple rotary switch of the "minor switch" type, and is set by the decimal equivalent of the first letter. This connected the impulse circuit to the vertical magnet, then to the rotary magnet of the selected *BC* register. This Strowger switch registered the *B* and *C* digits by its "up and around" movement of four wipers.

Now consider Figure 11 which illustrates the translation of the decimal equivalent of the office code.

Each Director has one code *A* register, to whose bank are wired eight code *BC* registers. I have illustrated this by only three of them. From the *A* register bank contacts the wires run to the magnets of the *BC* switches. The wipers of all the *BC* switches are multipled, each to each, and run to the bank of the sender sequence switch. The banks of the *BC* switches are wired separately to one side of an intermediate distributing frame, *IDF*. The other side of that *IDF* is wired to the bank of the sender switch, where the sending of impulses over the loop will be governed. In this simplified illustration I have shown only two wipers for each *BC* switch. There were six wipers on each switch as planned for London.

Figure 9. Rotary switch

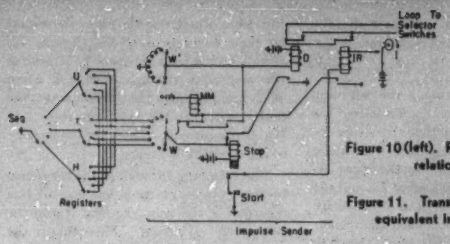


Figure 10 (left). Register to sender relationship

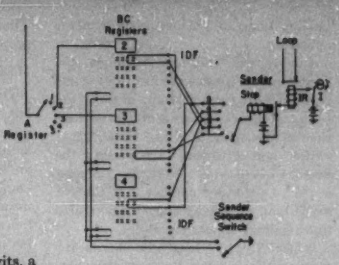


Figure 11. Translation of decimal equivalent into directive

Let us assume the following relations.

Decimal Equivalent	Directive
243	32
324	41
433	53

On the *IDF*, jumpers would be run as shown. For example, the number 2 *BC* switch bank contact 43 would have its bottom contact of the pair wired to *3* on the right side and its upper mate wired to *2* on the right side. The other jumperings are self-evident. In practice all the bank contacts of the eight *BC* switches are similarly jumpered, according to the pathways which each interoffice call must take.

If the call which we are considering be *243*, the *A* switch will be on its number 2 bank contact, and the number 2 *BC* switch will have its wipers resting on its fourth level, and the third pair of contacts. Now when ready to translate and send, the sender sequence switch will move one step, thus grounding all the lower wipers of the *BC* switches. Only the lower wiper of the number 2 switch is connected—the others are normal and open. This move of the sequence switch starts the sender which performs as described in connection with Figure 10 and cuts off when three impulses have been sent. Then the sequence switch, Figure 11, will move to its next contact, switching the ground to the upper wiper, and start the sender on its next round.

The performance is similar for any other decimal equivalent.

In the actual case of four wipers for each *BC* switch, the sender can send four digits in the directive. The maximum number of digits in the directive fixes the number of wipers needed.

It is the ability to move to a desired position any reasonable number of wipers that gives to the Director the possibility of sending out almost any kind of directive, one digit for each wiper which has been set. If the office code has only one digit, and the directive might have as many as four digits, a minor switch (rotary motion only) with four wipers can do it. If the office code has two digits, a Strowger switch (two motions, up and around) will serve, with as many wipers as the largest number of digits in the directive. For the usual 3-digit office code, a minor switch for the *A* digit and a Strowger switch for the *B* and *C* digits will serve, the Strowger switch having as many wipers as the largest number of digits in the directive.

Having seen how the call number is registered and how several functions work, let us see how they are controlled and co-ordinated in sending out the directive. Assume a call number *324-5342*. According to Figure 4 the first digit has rotated the *A* register three steps, the next two digits have lifted the wipers of the *BC* register two steps and rotated them to the number 4 set of bank contacts. Further, the dialing of the terminal number has set the wipers at contacts 5-3-4-2, respectively, as shown in Figure 12.

Assume that to reach the distant office the Director must send the digits *4* and *1*. The bank contacts of the *BC* switch had been cross-connected to the decimal terminals *4* and *1*, on the *IDF* (at the time installation).

Thus we have in the register bank multiple a complete representation of the digits which are to be sent, and the bank of the sender switch carries this bank multiple with the wires in numerical order. The sender is ready to obey the indications thus presented.

There is one exception, the "skip digits." Because the decimal equivalent (*324*) of the office code requires only a 2-digit directive, the upper pair of wipers on the *BC* switch are not needed. They are cross connected on the *IDF* to two contacts on the bank of the *R* wiper of the sender sequence switch. As described later, this causes the sequence switch to pass by those wipers quickly without waiting for any impulse sending.

Sending the Impulses

Just as was illustrated by Figure 10, we are to govern the number of impulses sent in each digit by means of a grounded contact on the bank of a register. Figure 12 shows all eight wipers, four in the *BC* register and four registers for the line number. The ground is to be placed on those wipers in succession, by means of wiper *S* of the sender sequence switch. Each time that the sequence switch advances and thus grounds a new wiper, it starts the sender on a round of exploration by means of the "start sender" wire. When the sender switch has made a cycle and returned to the home contact, it advances the sequence switch by means of the "sequence" wire.

To avoid too much complexity I have omitted from Figure 12 all the details which could be spared, trying to leave only those which will make clear how translation of the office code and the sending of impulse digits are done.

The *BC* register is controlled by the calling subscriber through a line relay *LR* in the trunk repeater. Assume that the wipers of the *BC* switch have been lifted by the second digit. As the line relay repeats the impulses sent by the subscriber, the rotary magnet *RM* steps the wipers around. At the same time the series relay *Se* pulls up and remains operated. It operates relay *F*, which locks to a ground in the trunk repeater. When the third digit ends, *Se* releases and grounds the start wire for the sequence switch. Wiper *R* connects to the self-vibrating contact of the motor magnet *MM-1*, which promptly advances the sequence switch wipers one step. This put the *S* ground on wiper number 1 of the *BC* switch, and at wiper *SW* the same start wire ground is applied to the impulse relay *IR* of the sender. As was described for Figure 10 the interrupter *I* acting through *IR* (Figure 12) steps the sender switch around to contact 4, having thus sent four impulses over the loop to the first selector.

At the beginning of impulsing, relay *D* pulled up to clear the loop for *IR*, and also pulled up *MM-1* of the sequence switch,

holding it operated as long as the sender is operating.

Having sent the four impulses, the sender switch rotates around to its home position, both bank contacts of which are open. Relay *D* releases and thus releases the motor magnet *MM-1* of the sequence switch, and advances its wipers to the next set of contacts. Here the *S* ground is placed on wiper number 2 of the *BC* switch and the sender started again, this time sending only one impulse.

Wiper *SW* has been the means for starting the sender. Its next two contacts are open, but the corresponding contacts of wiper *R* have "skip" wires, leading to the bank contacts of the *BC* switch which are occupied by wipers 3 and 4. Hence the ground applied by wiper *S* operates to cause the motor magnet *MM-1* to rotate wiper *S* (and all the others) quickly past wipers 3 and 4 without sending any impulses to the loop.

Placing the start wire of the sequence switch into the control of the *BC* switch is to insure that the subscriber has dialed all of the office code. The directive can not be specified until all the code digits are registered. But after that time, the directive can be sent, without waiting for the registry of the line number.

But the terminal number, though sent out as received, can not be sent until it is received. For this reason, the starting of the sender is now changed to off normal springs *ONS* on the line registers. When any one of them is dialed away from its normal position, the *ONS* grounds a wire leading to the bank of the wiper *SW*. Thus the sender can send each digit in succession as fast as it is registered.

Holding Circuits and Release

During the registering of the call number and the sending of the directive, ten switches are held, either passively or actively. The holding circuits for these are shown in Figure 13. In the upper left hand corner are the line relay *LR* and the associated slow relay *6*, both of which are located in the trunk between the incoming line and the first selector. The slow relay holds the trunk from releasing, even while *LR* is impulsing into the director.

The main holding circuit passes from the slow relay *6* to the finder switch, thence through the register sequence switch, the code *A* register, ending in the *BC* register switch. In the finder switch, relay *9* is holding the connection through its wipers, in the *BC* switch relay *11* is withholding ground from the release magnet. The *10* relay in the register sequence switch is controlling the release of six switches, the sequence switch, the *A* register, and the four line registers, *Th*, *H*, *T*, and *U*. In addition, it is in a position to ground the *W-3* bank of the sender sequence switch to chase it home.

In Figure 13 we note also the means to switch the subscriber line through to the selectors and thus to the called telephone. Relay *7* is the through-switching relay in the trunk circuit. It is connected through the finder switch to the sender and the sender sequence switch as shown.

As soon as the sender reaches home from sending the last digit of the call number, relay *D* of the sender (Figure 12) falls back quickly, but the stop relay hangs on for a moment. The release of *D* permits the sender sequence motor magnet *MM-1* to release, which quickly snaps the wipers to their contacts *S*. This (Figure 13) completes the path between the ground at the stop relay through bank and wiper *W-2*, again through the sender circuit, the finder switch, to the switching relay 7 in the trunk circuit. Then the stop relay of the sender falls back.

The switching through of the telephone to the train of switches has cut off line relay *LR* of the trunk, so that it and the slow relay *6* release. This takes the control trunk ground from the entire system of five relays, affecting seven release magnets. Relay *9* severs the finder switch connection, relay *11* operates the release magnet for the *BC* switch, relay *10* of the register sequence switch operates the release magnet of the *A* register, and all four of the release magnets of the line registers. In each such case, the release magnet is cut off by an off normal spring, which breaks its circuit when the switch reaches normal.

Another ground contact on relay 10 grounds the bank of wiper *W-3* of the sender sequence switch, whereupon the motor magnet *MM-1* interrupts its own circuit and steps all its wipers around to the home position.

The Director is now ready to care for another call.

In spite of the many words which I have used in this explanation, the entire release time is very short, usually not more than one-half to one second.

The Later Director

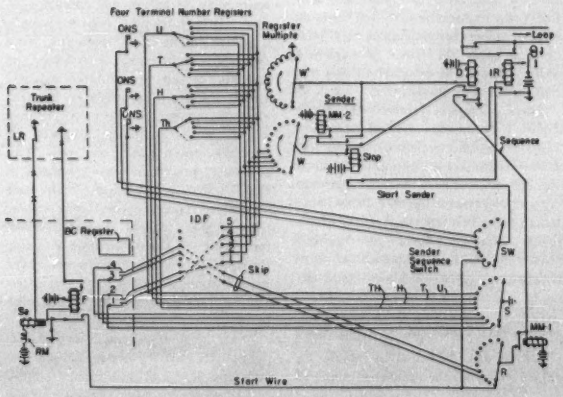
From time to time, improvements have been made in the Director. Out of them have come some very interesting structures of circuits resulting in economy of apparatus and greater flexibility. In the arrangement which I will now describe, part of the apparatus is common to a number of directors, being released as soon as its work has been done.

In Figure 14 the telephone *Telo.* is connected through nonnumerical switches *Non.* to an idle first selector. Out of the associated relays come several wires to a director hunter by means of which an idle director is found and seized.

At the right are the registers in which are to be stored the information which shall guide the sender in sending groups of impulses to the train of numerical switches leading to the distant office and subscriber line.

The dialed impulses pass through a relay *A1*, which repeats them to the magnets of the registers. There is a series slow relay which pulls up on the first impulse, remains energized, and after

Figure 12. Registers and sender details assembled



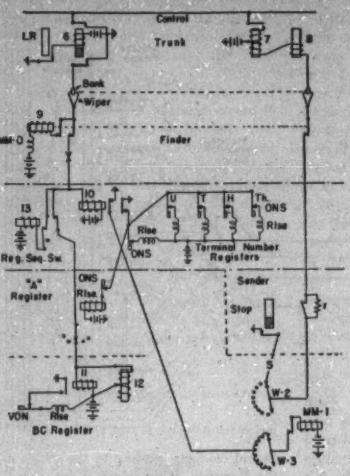


Figure 13 (left). Release circuits

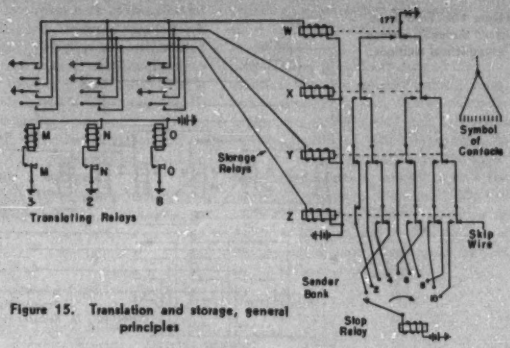


Figure 15. Translation and storage, general principles

the digit is registered, releases and causes the register sequence switch *Reg. Seq.* to advance one step. Three office code digits are followed by the line number, which the register sequence switch delivers to the motor magnets of the four line registers in succession.

The wiper and bank marked *Reg. Seq.* have wiper *U* on the same shaft. During the latter part of its rotation, this switch acts as the units register of the line number.

The storage relays with their sequence switch will handle the directive and later will feed it into the decimal multiple.

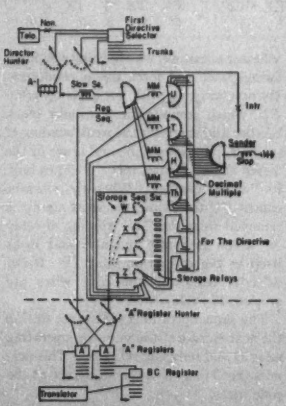


Figure 14. Improved Director, general plan

Below the dashed line are four sets of apparatus which are common to as large a number of directors as the traffic needs. First is the *A* register hunter which finds and seizes an idle *A* register. This is a Strowger switch, whose vertical motion registers the *A* digit of the office code. Its wipers then rotate to find an idle *BC* register. The latter responds to the *B* and *C* digits of the office code, and connects the circuits to the translator *Trans.* which is associated with it for that particular set of bank contacts.

The dialing of the terminal number is now switched to the magnets of the registers, *Th*, *H*, *T*, and *U*.

Upon completion of dialing the *ABC* digits, the translator at once sends back through other wipers of the *BC* and the *A* registers the complete designation of the directive which will guide the call to the desired office and line. This directive is delivered to the storage sequence switch 4 wipers, which guides each digit to its group (four relays each) where it is held by the locking up of the relays which were energized. The output contacts of each group of four relays are represented by a delta-shaped symbol at the right of each, which contacts lead into the common decimal multiple cable. The directive may have few or many digits. See Figure 15 for the meaning of the symbol.

The result is that we have connected to the decimal multiple a number of settings equal to the four terminal digits and to the number of digits in the directive.

Now the apparatus below the dashed line may be disconnected, being needed no longer for the call which we are considering.

The sequence switch associated with the storage relays now takes control, connects a ground on the first directive setting, and starts the sender. The latter sends impulses into the loop leading to the first directive selector, which stops when the wiper arrives at the grounded contact. The stop relay ends the impulses and causes its sender sequence switch to influence the storage relays to ground the next directive setting. It results in the next directive being sent, with the number of impulses indicated by the existing ground on the decimal multiple. In the same way all the rest of the directive digits are sent.

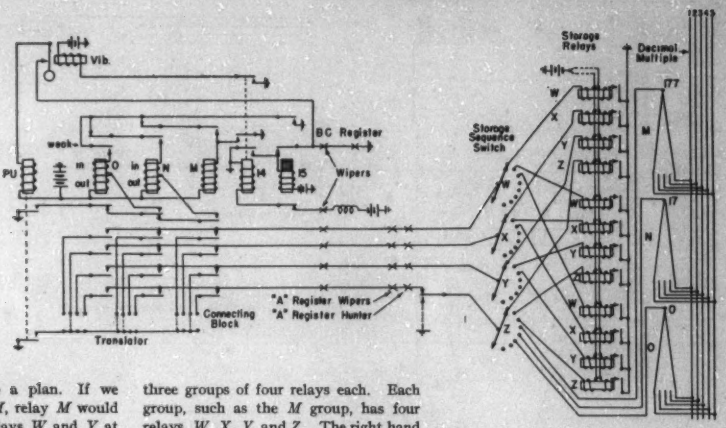
In like manner the terminal registers controls the impulses in its digit, until the entire number has been transmitted. At this moment, the wipers of the connector in the distant office are resting on the contacts of the called line. The events which follow are common knowledge.

Translation and Storage

The circuit device which forms the foundation is illustrated by Figure 15. Consider the four relays at the right and their contacts, mostly arranged in series with each other. If all four are energized, there is no path from the grounded spring to the sender bank. If all four relays are normal, there is a path from the grounded spring to the skip wire, which leads to that bank of the storage sequence switch to whose wiper the motor magnet is attached by its self-vibrating contact. It enables fewer directive digits to be used than the maximum provided.

Now consider the three relays at the left, and their contacts. Each relay has four main springs, one for each of the relays at the right. But the main springs are

Figure 16. From translator through storage into decimal multiple



grounded according to a plan. If we should press the key *M*, relay *M* would ground and operate relays *W* and *Y* at the right. Now trace the path from the main spring of relay *W* downward to the sender bank—the number 3 contact is grounded. Hence, if the sender moves its wipers as usual, the stop relay will pull up when the wiper reaches number 3.

Notice that the transfer of information is practically instantaneous—the storage relays pull down together.

If we had pressed the *N* key, it would have grounded contact number 2 on the sender bank. Likewise the *O* key would have grounded the number 8 contact. Thus by pressing any key we can cause the sender to send a certain number of impulses before being stopped. Reference to Figure 7 will recall the action.

The complete code is this:

Relays operated	*W*	*WX*	*WY*	*WZ*	*X*	*XY*	*XZ*	*Y*	*YZ*	*Z*
Digit produced	*1*	*2*	*3*	*4*	*5*	*6*	*7*	*8*	*9*	*0* or *10*

It will be observed that the four wires between the *MNO* relays and the *WXYZ* relays are few enough to permit much switching without too many wipers. This is needed, for these four wires must be carried back (Figure 14) through the banks and wipers of the *BC* register, the *A* register, the *A* register hunter to the storage relays which feed into the decimal multiple into which the sender bank is wired.

To make possible later diagrams I will use the symbol at the right in Figure 15.

The essentials of the transfer of a translation to the storage relays are shown in Figure 16. I am assuming that the directive has only three digits, *M*, *N*, and *O*, represented by the relays at the left. Beside their operating contacts, each of them has four main springs, corresponding to the four wires, *W*, *X*, *Y*, and *Z*—the basis of the transfer code.

At the right are the storage relays, three groups of four relays each. Each group, such as the *M* group, has four relays, *W*, *X*, *Y*, and *Z*. The right hand winding of each with its contact enables each to lock itself energized. The left hand windings are the pull-up windings, by means of which the codes coming from the translator will operate such storage relays in each group as the desired decimal digit requires. It is the storage sequence switch which delivers the code for each digit to its own group of relays.

In the translator, the three relays *M*, *N*, and *O* must be caused to operate and release in sequence, so that each may ground the desired wires. At the bottom I have shown a small connecting block, by means of which the four main springs of each relay may be wired to the ground bus bar to make the code. For instance, suppose that the necessary directive is *325*. Thus when relay *M* pulls up, it must ground wires *W* and *Y*, shown by the dotted line on the block. Likewise, relay *N* will momentarily ground the *W* and *X* wires. Then relay *O* will momentarily ground the *X* wire alone.

By a control circuit not shown, the storage sequence switch will step its wipers along. When relay *M* is pulled up, the wipers of the sequence switch will be resting on their first contacts, leading to the four storage relays in the *M* group. Hence only relays *W* and *Y* will be pulled up and lock. The contacts represented by the delta-shaped symbol (Figure 15) will connect spring *m* to wire 3 in the decimal multiple.

When relay *N* in the translator (Figure 16) is taking its turn, the wipers of the sequence switch will be resting on their second contacts, leading to the relays in the *N* group. Thus relays *W* and *X* will operate and lock. When relay *O* in the translator takes its turn, the sequence switch will guide the ground connection to relay *X* in the *O* group, storage.

The sequence operation of the *M*, *N*, *O* relays in the translator is accomplished as follows. When the *BC* register stopped its wipers on the set of contacts, relay *14* pulls up in series with a relay in the *BC* switch. Relay *14* pulls the armature of the vibrator, *Vib.*, and holds it there, operates relay *M*, and pulls up relay *15*. The latter locks itself, grounds the holding circuit for relay *M*, and cuts off relay *14*. On falling back, relay *14* releases the vibrator armature, which, being weighted, swings back and forth like a pendulum. Its first swing to the left pulls up relay *PU*, which grounds the translator block bus bar and energizes the weak winding of relay *N*. Relays *M*, *N*, and *O* are of the 2-step type. Hence, relay *N* closes only its "weak" contact. When the vibrator causes *PU* to release, the short circuit is removed from the "in" winding of relay *N*, which promptly operates and locks itself to the ground on relay *15*. Relay *M* falls back and the code wires are grounded by relay *N*.

The same operations take place during the next closure of the vibrator, operating relay *O* and releasing relay *N*. This permits the *O* relay to have its chance at the code wires.

The last digit of the directive having thus been stored, the circuits are cut off at

the sequence switch, so that the *A* register, *BC* register, and translator are released and return to normal, ready to be seized by another call.

In the storage sequence switch, the *Z* wiper is switched to a circuit where it gets ground. From here this part acts like a sender sequence switch, working with the sender (Figure 14) in applying ground to each of the directive springs, *m*, *n*, and *o* in turn, and afterward to the wipers of the line register, *Th*, *H*, *T*, and *U* in succession.

Incidental Functions and Services

Not all calls go through with the regularity which I have assumed. Some of the following incidentals must be expected:

1. The subscriber removes the handset but does not dial.
2. He dials only part of the call number and then stops.
3. He dials a nonexisting office.
4. He dials a dead line number.

There is not room here to describe all these details, but adequate means have been provided. The first two are handled by a slow time device, which comes into play if any director is held longer than a set limit, after which the connection is released. Number 3 is run off to an information desk. The fourth is handled on the banks of the connectors as now done in automatic exchanges.

Trunking to manual offices, which have call indicators to display the number in front of the operators, is handled by inserting a coder in the trunk beyond the first selector, or a later selector. This changes the decimal digits into the kind of code used and sends it to the manual switchboard. Thus it does not affect the structure of the director. Incoming calls from manual to automatic are sent by the operator using some kind of calling device, either the push-button type or the dial type (for small manual boards). Such calls go directly into the automatic switches—the Director is not involved.

Enough has been illustrated and described to show rather conclusively that some kind of translating device can be made which will, from any office code, pick out any prescribed decimal number, and that this can reliably control an impulse sending device. Communication engineering is especially rich in devices and combinations. These being true, we need not care very much how the interoffice selectors are arranged, or how the multioffice exchange is laid out. We can make the complete Director assembly do whatever is necessary to direct a call to the desired distant office and to connect with the desired telephone.

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2. Fundamentals, Strowger Dial Systems, Automatic Electric Company, Bulletin 301.

On the Director

3. The British Post Office and Automatic Telephones, Colonel T. F. Purves, engineer in chief to the British Post Office. Especially pages 639–49.

No Discussion

An X-Ray Thickness Gauge for Hot-Strip Rolling Mills

C. W. CLAPP
NONMEMBER AIEE

R. V. POHL
MEMBER AIEE

THE EQUIPMENT described in this paper was built to fill a long-felt need in hot-strip steel-rolling mill practice. Heretofore, all gauging of the strip thickness has, of necessity, been delayed until the strip has cooled to a temperature where measurement by a hand-held micrometer is possible. In this length of time a modern high-speed mill may roll six or more tons of steel. Any error in mill setting resulting in an off-gauge strip is therefore very costly because of the large amount of off-gauge material produced before the error can be corrected. The need for equipment to gauge continuously the thickness of the strip as it issues from the finishing stand has been obvious for some time.

By ordinary gauging standards, the conditions under which the thickness measurement must be made are somewhat unusual. In a typical steel mill, the strip may be travelling at speeds of 2,000 feet per minute or more out of the finishing stand. As it leaves the rolls it may vibrate vertically through an amplitude of several inches and to prevent the possibility of jams or "cobbles," the clear opening through the gauge should be at least 18 inches high. The temperature of the strip is ordinarily between 1,500 and 1,750 degrees Fahrenheit. Cooling water sprays deluge the strip and any nearby equipment.

Under these conditions, a successful solution to the problem has had to await the development of a new X-ray gauging technique which forms the subject of this paper.

Review of X-Ray Theory

X rays are electromagnetic radiations lying in the wave length range extending from about 10^{-7} centimeter to about 10^{-12} centimeter. They are most readily produced by allowing a beam of high-speed electrons to fall on a target of some heavy element such as tungsten. When the electrons in the beam have an energy acquired by falling through a potential difference of V volts, the distribution in wavelength of the resulting X-ray energy is as shown in Figure 1, where λ_0 the shortest wavelength present, is given by

$$\lambda_0 = \frac{12{,}350}{V} \text{ angstroms} \tag{1}$$

where 1 angstrom unit $= 10^{-8}$ centimeters. These curves depicting the "continuous" X-ray spectrum[1] ignore the presence of any characteristic radiation emitted by the atoms of the target as this is an effect of which we make little use at present. The total X-ray energy radiated varies approximately as the square of the accelerating voltage V, directly as the beam current I, and directly as the atomic number of the target material.

When a beam of X rays strikes a sheet of some absorbing substance such as steel, a part of the beam is transmitted through the sheet unchanged while the remainder is either absorbed within the sheet or is reradiated as scattered X rays. When the incident beam is essentially monochromatic, the energy in the transmitted beam is given by the following equation

$$I = I_0 e^{-\mu x} = I_0 e^{-(\mu/\rho)\sigma} \tag{2}$$

where

I_0 = energy in incident beam
I = energy in transmitted beam
x = thickness of absorber (centimeters)
μ = absorption coefficient (per centimeter)
ρ = density of absorber (grams per cubic centimeter)
(μ/ρ) = mass absorption coefficient (square centimeter per gram)
$\sigma = (\rho x)$ = surface density of absorber (grams per square centimeter)

The mass absorption coefficient (μ/ρ) which appears as a constant in equation 2 is in turn a function of the wave length of the X rays and of the kind of atoms composing the absorbing material. Figure 2 shows the way in which the coefficient varies with wavelength for a few typical materials. Within regions which do not include a discontinuity, the mass absorption coefficient varies approximately as the cube of the wave length (λ), and approximately as the cube of the atomic number (N) of the absorbing atoms.

As yet we have said nothing about the means used to detect and measure the energy in the X-ray beam, though this is, of course, a matter of prime importance. Considering such qualities as sensitivity, speed of response, spectral response, long life, and convenient operation, a very satisfactory detector can be obtained by the use of a phosphorescent screen directly in front of a sensitive phototube.

Such a detector was used to obtain the data for Figure 3 which shows the intensity of the X-ray beam transmitted through different thicknesses of a standard low-carbon steel for several values of voltage V applied to the X-ray tube. In this case, the tube was operated directly from a 60-cycle high-voltage transformer and V represents the peak value of the voltage across the tube during the half-cycle when the target is positive with respect to its cathode.

The curves of Figure 3 are basic to the design of a thickness gauge for the material in question. They show for example that for a particular steel thickness and tube voltage represented by the point A, a 10 per cent decrease in tube voltage has

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C. W. Clapp and R. V. Pohl are both with the general engineering and consulting laboratory, General Electric Company, Schenectady, N. Y.

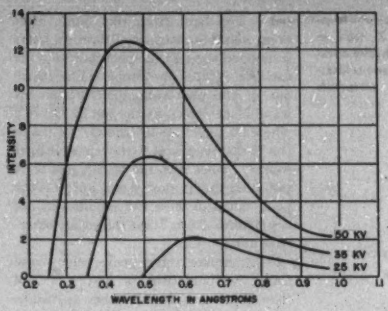


Figure 1 (above). Continuous X-ray spectra at different constant potentials (after Ulrey)

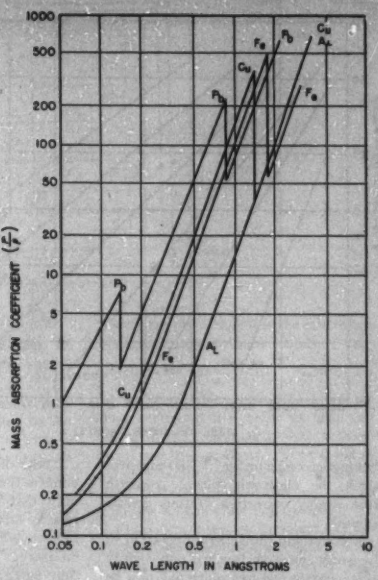


Figure 2. Mass absorption coefficient as a function of wave length

the same effect on the detector as a 27 per cent increase in steel thickness. Effective means must therefore be found to stabilize or compensate for changes in tube voltage to secure a reasonable accuracy in gauge reading. The curves also show that for a given small change in steel thickness the corresponding change in detector reading is not constant but decreases in general as the thickness of steel is increased. This behavior complicates the design of a gauge having a linear scale over a wide range of thicknesses. In the gauge to be described, solutions have been found for these problems and a linear thickness scale has been obtained over the range from 0.04 to 0.19 inch of steel with an over-all accuracy of ±2 per cent.

Description of Gauge

A condensed description of the method of operation of the gauge will first be given with reference to the block diagram Figure 4, following which the more important features of the design will be discussed in greater detail.

Let us begin at the X-ray tube shown in Figure 4. Ports are provided in the enclosure for this tube to select two beams from the total radiation to be designated as the reference beam and the measuring beam respectively.

The reference beam first passes through a long wedge of absorbing material (standard wedge), then falls on radiation detector number 1. The output of the detector is a direct voltage whose magnitude and polarity are a measure of the difference between the intensity in the reference beam and a standard reference intensity established within the detector for comparison purposes. The output voltage of this detector is used to control an electronic-voltage regulator in the primary circuit of the high-voltage transformer supplying the X-ray tube. By thus automatically controlling the voltage on the X-ray tube, the intensity in the reference beam is maintained at a level such that, after partial absorption in the standard wedge, it will have the value determined by the reference standard in detector number 1.

The measuring beam first passes through a thin wedge of absorbing material (measuring wedge) then through the thickness of steel to be measured and falls on the radiation detector number 2. Again the output of this detector is a direct voltage measuring the difference between the intensity of the beam striking the detector and a standard reference intensity established within the detector for comparison purposes. The output voltage of this detector is in turn used to control a motor geared to the measuring wedge. When the intensity in the measuring beam is different from the reference intensity, the motor shifts the wedge in a direction to increase or decrease the absorption in the beam as required to maintain the intensity at the value determined by the reference standard in detector number 2.

In operation, the reference standards in the two detectors are adjusted so that the measuring wedge comes to rest when the total absorption in the path of the reference beam equals the total absorption in the path of the measuring beam By making both wedges of steel, the thickness of the unknown steel sample can be obtained simply as the difference between the effective thickness of the two wedges. To provide a simple means of reading the gauge, the positions of the two wedges are transmitted through separate "selsyn" systems to two-scale pointers on a remote-indicator panel. One of these pointers, connected to the standard wedge, indicates the nominal steel thickness to which the gauge is set. The second pointer, connected to the measuring wedge, indicates on a zero-center scale the deviation in sample thickness from the nominal gauge size.

To set the gauge to a different value of nominal steel thickness, the standard wedge must be moved to present a different thickness to the reference beam. This is done by means of a small reversible motor controlled by push buttons on the remote-indicator panel. The same motor is also linked through a cam-mechanism to the arm of a variable-ratio autotransformer connected in the primary of the X-ray high-voltage transformer. As the standard wedge thickness is altered, the voltage on the X-ray tube thus is adjusted automatically to the approximate value needed to maintain the required intensity in the reference beam

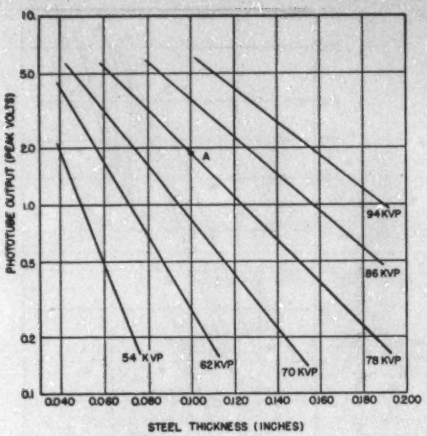


Figure 3. Absorption of X rays in steel with tube operated at various alternating potentials

striking detector number 1. The range over which the electronic-voltage regulator must supply correction is thus greatly reduced.

In describing the general operation of the gauge, we have touched only lightly on two circuit features which will now be discussed more fully.

The circuit of the radiation detector is shown in Figure 5. This employs a 931-A multiplier-type phototube with a thin layer of silver-activated zinc-sulphide phosphor cemented to the outside of the phototube envelope opposite the cathode surface. When excited by X rays, this phosphor emits light having most of its energy at a wave length of 4,350 angstroms. Since the 931A phototube has its peak response at 4,200 angstroms, this makes a reasonably sensitive combination. Phototubes of this type vary widely in sensitivity but average tubes so coated have been found which when placed in a beam of 50 milliroentgens per hour gave a signal 30 decibels above photo tube noise. For these tests, the X-ray tube was operated in a half-wave self-rectifying circuit at a voltage of 40-kv peak value and the signal was measured in terms of peak-to-peak value.

As a radiation detector, the phototube-phosphor combination has the advantages of high sensitivity, high speed of response, and reasonably long life, and it does not require the maintenance of very high resistance circuit elements as does the ionisation chamber method. On the other hand, the sensitivity of the multiplier-type phototube varies rapidly with changes in electrode voltage, and is somewhat subject to fatigue effects.[2]

These disadvantages have been overcome in the present detector by requiring only that the phototube compare the relative strengths of two light signals and indicate which of the two has the greater intensity. The first of these signals is that due to the excitation of the phosphor by the X-ray beam and consists of rather sharply peaked half-wave pulses occurring at line-voltage frequency. During the half-cycle when the voltage on the target of the X-ray tube is negative, no X rays are emitted and the phosphor is dark. The second light signal is obtained from a glow-discharge lamp of a type recently developed for light-modulator service. This lamp is connected to the 60-cycle line through a ballast resistor as shown in Figure 5, and is shunted by a diode rectifier to ensure its excitation only on the alternate half-cycles of line voltage when no X rays are being emitted. The light from this lamp, after being suitably attenuated through filters is directed through the phosphor onto the cathode of the phototube. The signal out of the phototube therefore has the wave shape shown at the top of Figure 6 where the peaks are alternately related to the X-ray beam and to the reference light source. Since it is the peak value of X radiation that is most significant, it is the peak values of these two signals that are compared. Their wave shapes are otherwise unimportant.

To compare these peaks, the signal from the phototube is first amplified through a linear amplifier, then applied to the grids of the phase-sensitive triode detectors as shown in Figure 5. In this circuit, the plates of the two detector tubes are supplied with voltages of equal amplitude but opposite phase through a center-tapped transformer connected to the 60-cycle line. The cathode-load resistors R_1 and R_2 are made very large compared with the reciprocal of the mutual conductance for the triodes and the capacitors C_1, C_2 are selected so that the time-constants $R_1C_1R_2C_2$ are long compared with the period of the signal wave. Under these conditions, the capacitors C_1 and C_2 charge up to steady voltages V_1 and V_2 sufficient to maintain their respective tubes close to or beyond plate current cut-off at all times. If we designate the a-c component of the voltage applied to the grids of the tubes in parallel by e_g, and the a-c component of the plate voltages by e_{p1} and e_{p2} as shown in Figure 6, then we have the condition that

$$e_g - V_1 + (1/A)(e_{p1} - V_1) = 0 \qquad (3)$$

and

$$e_g - V_2 + (1/A)(e_{p2} - V_2) = 0 \qquad (4)$$

where A is the amplification factor of the triodes.

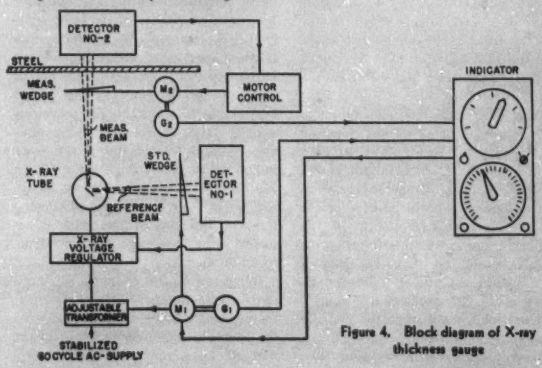


Figure 4. Block diagram of X-ray thickness gauge

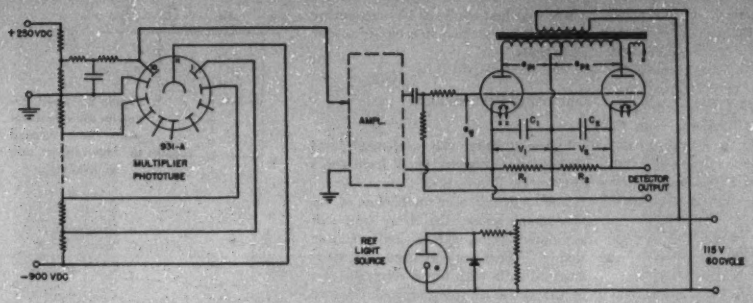


Figure 5. Circuit diagram of radiation detector

$V_1(1+1/A)$ is equal to the maximum value of $e_g+(1/A)e_{p1}$

$V_2(1+1/A)$ is equal to the maximum value of $e_g+(1/A)e_{p2}$ ■

Remembering that the plate voltage on one of the triodes, say e_{p1}, has its maximum value when the signal from the X-ray beam is a maximum, and that the plate voltage on the second tube (e_{p2}) reaches its maximum in phase with the signal from the reference light source, it can be seen that V_1 and V_2 are linear functions of the peak values of the X-ray signal and the reference light signal respectively. The output of the detector, consisting of the voltage (V_2-V_1), is therefore zero when the two signals have equal peak values and at other times has a value proportional to the difference between the two signals.

The circuit diagram for the electronic regulator used to control the accelerating voltage on the X-ray tube is shown in Figure 7. This regulator operates by varying a resistance element connected in series with the primary of the high-voltage X-ray transformer.[8] The resistance element consists of the plate resistance of four 807 tubes connected in parallel. The transformer T_3 serves to provide a proper impedance match between the plate circuits of the tubes and the power-line circuit into which they operate. To utilize the 807 tubes efficiently, their screens are maintained at a steady d-c potential. The grid bias on the tubes is controlled by the output of radiation detector number 1 amplified through one stage of d-c amplification. When connected as shown the tubes carry current only on one half-cycle of the line voltage, this of course being the half-cycle during which the X-ray beam is emitted. The resistor R serves a dual purpose; it limits the voltage appearing across the tubes on the negative half-cycles of voltage and during the warm-up period of the tubes, and it increases the regulating range of the circuit as will be shown in the simple analysis to follow.

As normally operated, the current through the X-ray tube is limited by the emission of the cathode. We will therefore make the simplifying assumption that the current drawn by the high-voltage X-ray transformer has the constant value I_L during the positive half-cycle of voltage and is zero during the negative half-cycle. While the line current I_L is flowing, let I_R be the current through resistor R, and $E_R=RI_R$ be the voltage across R, and let I_p be the total plate current through the regulator tubes. If the transformer T_3 has the turns ratio n, and we neglect losses in this transformer, then $I_L=I_R+nI_p$, and the voltage on the plates of the regulator tubes is $E_p=nE_R$.

Figure 8 shows the various currents and voltages involved referred to the high-voltage side of transformer T_3.

Figure 9 shows the plate characteristics for an equivalent regulator tube. Since four *807*-type tubes are used in parallel, this is obtained from the characteristics of a single tube by multiplying the current values by four. The permissible region of operation of the tube is bounded by the heavy line *O-a-b-c-d-O*. The boundary *c-d* is determined by the maximum voltage that can be allowed across the tubes and transformer. The boundary *b-c*, a segment of a rectangular hyperbola, is determined by the maximum dissipation that can be allowed at the plates of the regulator tubes. In drawing this, it should be remembered that plate current flows only during one half of the cycle. The boundary *O-a-b* follows the curve of zero grid bias for the particular value of screen voltage used, since insufficient power is available in this circuit to drive the grids positive. The screen voltage on the tubes was limited in this instance by screen dissipation.

Since the plate current in the equivalent regulator tube is given by

$$I_p=(1/n)(I_L-I_R)=(1/n)(I_L-E_p/nR) \quad (5)$$

the various possible operating points lie along a straight line which we shall call the "load line" by analogy with conventional amplifier analysis. Since n and R are as yet undetermined, we are free to select both the slope of this line and its intercept on the plate voltage axis. Let the load line intersect the plate voltage axis at the maximum allowed plate voltage E_a, and let its slope be such that it intersects the boundary *O-a-b-c* at the point corresponding to the voltage E_1 and the

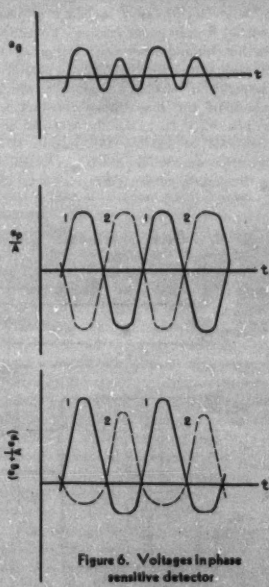


Figure 6. Voltages in phase sensitive detector

current I_1. Its intercept on the plate current axis is then the point I_L/n corresponding to full line current referred to the high voltage side of transformer T_3.

Let the total power dissipated in R and the regulator tubes be W_1 when the operating voltage has its minimum value E_1 and let it be W_2 when the operating voltage has its maximum value E_2. Our problem is then to adjust the slope of the load line so that $(W_2 - W_1)$ is a maximum. It can be shown that

$$W_2 - W_1 = (E_2 - E_1)I_L/n = E_2 I_1 \qquad (6)$$

hence I_1 should be as large as possible.

From practical considerations, a good choice is made by passing the load line through the point a just above the knee of the zero-grid bias curve. The intercept of this line on the plate current axis then determines the proper transformer turns ratio n and the slope of this line determines R through the relation $R = E_2/nI_L$.

In the regulator here described, I_L had the approximate value seven amperes, and suitable values for R and n were 8 ohms and 14, respectively. With these values, the voltage E_R across R could be controlled over the range extending from 15 to 35 volts. Hence an error in input voltage of ±10 volts can be corrected by this regulator and this range is ample. The sensitivity of the detector and d-c amplifier is such that a change in input voltage of ±5 volts produces a change of only ±0.001 inch in gauge reading. To reduce further this and other sources of error, all 60-cycle power for the gauge is obtained through an auxiliary regulator which maintains the line-voltage constant to within ±1 volt. The only troubles encountered in putting this circuit into operation were from parasitic oscillations in the parallel-connected regulator tubes. These were best eliminated by connecting a 2.5-millihenry choke in each of the four plate leads.

Operating Performance

Figure 10 shows the component parts of the gauge as installed at the Irvin works of Carnegie Illinois Steel Corporation.

The large unit near the bottom of the photograph houses the X-ray tube and associated high voltage and filament supply. These are contained in a lead-lined oil bath, with forced-oil circulation to the anode of the tube and separate water cooling of the oil. The tube used has a tungsten target and is rated at 100-kilovolt peak. Also contained in this unit are the two steel wedges mentioned previously, with their associated motor drives and connected "selsyn" transmitters, and the phototube and reference light source for detector number 1.

The phototube and reference lamp for detector number 2 are mounted in a separate water-cooled steel housing shown near the top of the photograph. At installation, this unit is mounted about 24 inches above the lower unit and is centered in the measuring beam which is directed vertically upward from the lower unit.

The cabinet shown at the right houses the remainder of the electronic detection and control equipment as well as the power supplies for all circuits.

The unit shown at the left is the remote indicator. Its lower dial indicates the nominal gauge size in thousandths of an inch, while the upper dial indicates the deviation of the steel thickness in thousandths of an inch from the nominal size. Means are provided at the indicator station to correct the calibration of the gauge when necessary and to select any nominal gauge size within its range.

There are certain points that should be considered in using a gauge of this type

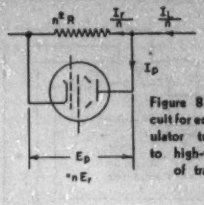


Figure 8. Load circuit for equivalent regulator tube referred to high-voltage side of transformer

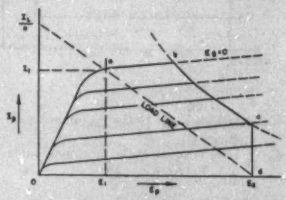


Figure 9. Plate characteristics of equivalent regulator tube showing location of load line

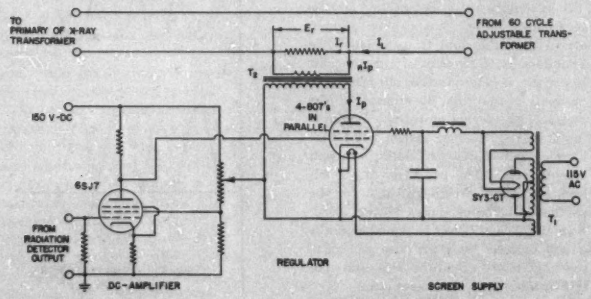
which are related to the operating conditions. Among these we can list the effect of impurities or changes in composition of the steel, the additional absorption of the layer of cooling water retained on the steel strip, and the contraction of the steel in cooling down to room temperature.

Fortunately, most of the alloying elements commonly used in making steel have an X-ray absorption coefficient less than or only slightly greater than that of iron. Tungsten and molybdenum are the principal exceptions to this statement, but are not commonly used in sheet steels. Stainless steel of the common 18–8 composition has an absorption coefficient within 1 per cent of that of iron at room temperatures, and this small difference can be readily taken care of by a zero adjustment during calibration. For common grades of silicon steel the correction may amount to seven per cent. The gauge dial of a gauge used for measuring several alloys has several concentric scales.

Large quantities of water ordinarily are used to cool the finishing rolls and the strip as it emerges from the finishing stand and consequently water-puddles an eighth of an inch in thickness will frequently be found on the strip at the measuring point.

A continuous water layer 1/16 of an inch thick can cause an error of 0.0006 inch in steel thickness when the steel is 0.06 inch thick and an error of 0.0016 inch when the steel is 0.18 inch thick. The difference is due to the change in voltage applied to the X-ray tube for these two conditions. Where this is objectionable, the water can be removed from the measuring point by a strong air-blast.

Figure 7. Circuit of electronic voltage regulator



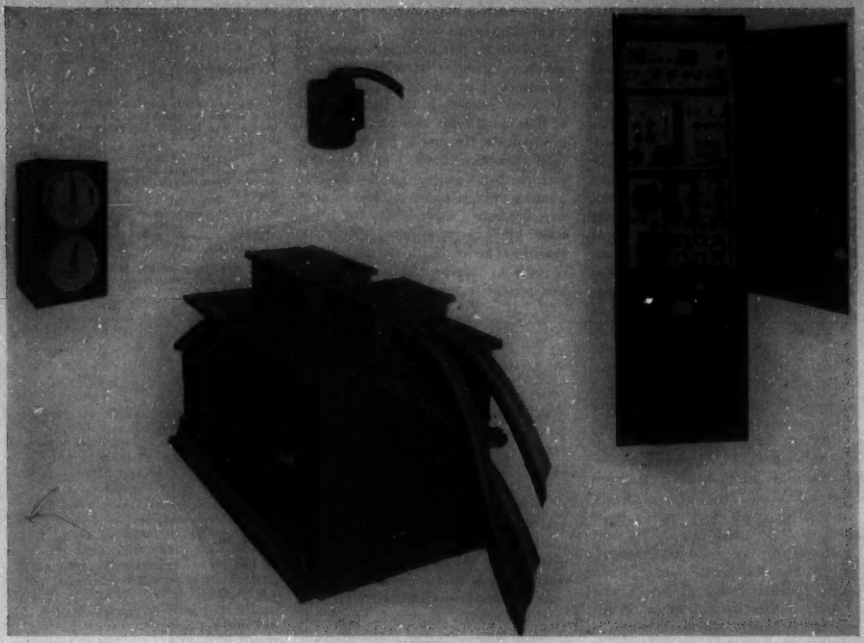


Figure 10. General Electric X-ray thickness gauge for hot strip steel mill

While the steel is being gauged, its temperature is in the neighborhood of 1,600 Fahrenheit. As compared with the same steel at room temperature, the hot strip presents fewer iron atoms in a given cross-section of the X-ray beam and hence gives a lower reading of thickness. The correction for temperature depends only on the expansion coefficient of the steel, and for ordinary carbon steels amounts to 1.6 per cent per 1,000 degrees Fahrenheit. Since the rolling temperature is usually known to within ±200 degrees, it is a simple matter to graduate the scales to correct for this error.

Safety Considerations

In a device of this type intended for use by nontechnical personnel, the question of safety must be given full consideration. Our approach to this problem has been threefold. First, the unit containing the X-ray tube has been built to conform to the requirements for a Class I or fully protective housing as defined in the proposed ASA Safety Code for the Industrial Use of X-Rays (*Z54.1–1946*). This means that, except for the measuring beam itself, the radiation from this unit is everywhere below the level at which it could cause harm to personnel. Secondly, by using sensitive detection means, the X-ray intensity needed in the measuring beam has been kept as low as possible. While a measurement is being made, the beam intensity at the upper phototube unit is approximately 50 milliroentgens per hour. Scattered radiation from the beam is below tolerance level of 12.5 milliroentgens per hour at all points more than two feet away from the direct beam under all conditions of use. Thirdly, warning lights and barrier gates interlocked with the X-ray tube voltage supply have been provided at all points of access to the gauging area to prevent personnel from entering this area while the beam is on. As an additional precaution and check, it has been recommended that operating personnel carry monitor pencils until the safety of their work habits has been amply demonstrated.

Other Applications

The gauge described in this paper was designed to fill a specific need—that of measuring the thickness of hot-rolled steel strip. The method employed is however adaptable to many other gauging problems where a noncontacting gauge is desired. One such application on which we are actively engaged is the gauging of cold-rolled steel strip where, because of the speed of the strip, the accuracy of contacting methods of gauging is reduced. The method can also be used with metals other than steel, and with various nonmetals. In all these applications, however, we must consider whether there is present in the material, in any appreciable quantity, an element whose absorption coefficient is greater than that of the element used as the basis of gauging. If such is the case, the thickness reading obtained on the gauge will depend on the relative amounts of the two elements present.

For example, the aluminum alloy designated as *24ST* contains approximately 4.5 per cent of copper. Unless care is taken to keep the X-ray tube voltage below

approximately 9-kv peak value, it will be found that this amount of copper is responsible for up to 35 per cent of the total absorption coefficient. A change of 1 per cent in copper content would therefore be read as a 9 per cent change in thickness, or alternatively, to obtain an accuracy of 2 per cent in thickness reading, it would be necessary to know the copper content to within ±0.2 per cent and to make appropriate correction. This could be accomplished by readjusting the zero of the gauge with a sample of proper thickness and composition in the beam. This alloy is perhaps the most difficult of the common aluminum alloys to gauge by this method, many of them presenting no problem in this respect.

To take another example, automotive gasolines contain tetraethyl lead in quantities up to 0.2 per cent by weight. At X-ray voltages in the neighborhood of 40-kv peak value, this amount of lead is responsible for over 25 per cent of the total absorption of this liquid. In this case, the X-ray gauge would be useless as a thickness gauge unless the lead content were accurately known. On the other hand, by-passing the liquid through a chamber of known depth, the method can be used to measure the percentage of lead present in the gasoline[4] with sufficient accuracy to make this an attractive method of analysis.

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No Discussion

Amalgam-Cathode Materials for Power Tubes

J. A. M. LYON
MEMBER AIEE

C. E. WILLIAMS
ASSOCIATE AIEE

Synopsis: The proper operation of ignitrons and other mercury-pool cathode tubes of conventional design may be impeded where these tubes are subjected to vibration, tipping, or rapid acceleration. These factors would, in general, be present in installations in electric locomotives, ships, or military tanks. In some cases arc-backs or loss of control would be caused by motion of the mercury. Many of the uses of the mercury-pool cathode tubes are predicated on the copious electron emission of mercury. It has been the aim of the research described in this paper to obtain a cathode which retains the tremendous emission capabilities of mercury and yet is mechanically rigid so that jarring or tilting of the tube does not affect the operation of the tube.

EARLY in this work it appeared that an amalgam would have the desired emission characteristics and the proper amalgam would have sufficient mechanical rigidity so that a tube could operate under rough usage involving rotating, inverting, or shaking of the tube. The first part of this problem is concerned with the materials and proportions for a suitable amalgam cathode and the second consists of devising a satisfactory mechanical arrangement for the tube elements, as well as providing a means of controlling the conduction of the tube. The amalgam cathode must be rigid and stable under the action of a cathode spot. Mercury is continually vaporized from the cathode spot and will condense upon the relatively cool walls of the tube. It is desirable to have an amalgam where any other vaporized constituent of the amalgam will be washed from the walls of the tube by the mercury and at the same time a reamalgamation will take place. These droplets of mercury and amalgam will be returned to the cathode and there reform into a solid amalgam. A further requirement of an amalgam cathode tube is that it compares favorably with conventional ignitron tubes on the basis of efficiency, overload capacity, useful life, ease of control, and ability to withstand inverse voltages without reverse conduction taking place.

Dentistry is probably the largest user of solid amalgams at the present time. Considerable information[1] is available on the hardness, setting time, composition, and resistance to corrosion of various dental amalgams. The majority of these contain a high proportion of silver. General information about amalgams is available in some chemical reference books,[2] and metallurgical texts.[3]

Very little has been published on the electrical properties of solid amalgams. Newman[4] has investigated the spectra of some alkali-metal amalgams; this work involved a glow discharge and at no time did he consider arc discharges. However, his work showed that the yellow lines of sodium were always present in the spectra of sodium-amalgam glow discharges. Very likely the amalgams used were liquid. Arons[5] studied the spectra and cathode drop of several metal amalgams. Arons' data on sodium amalgams are incomplete although he listed a cathode fall of 20 volts and stated that it was less than for mercury alone.

Amalgams which contain more than 99 per cent mercury are generally fluid at room temperature. Superficially, at least, this rules out the use of materials which have a very low solubility in mercury. Copper, iron, calcium, bismuth, antimony, aluminum, arsenic, and nickel have low solubility in mercury.[3]

The amalgam of sodium has some interesting properties which may allow the cathode to be replenished. An examination of the freezing point curve in Figure 1 shows that amalgams containing very high or medium percentages of mercury are liquid at temperatures of 50 Centigrade or less. Furthermore, amalgams containing from two to ten per cent sodium are solid at 250 Centigrade. Mercury vapor is continually condensing upon the walls of a tube and rolling back to the cathode. It is conceivable that if mercury and sodium deposited on the walls in any proportion greatly different from six per cent sodium, then the deposit would liquefy and return to the cathode.

The experimental equipment used in the research was composed of a vacuum system including a mechanical roughing pump, a two-stage mercury diffusion pump, a cold trap, and a McLeod gauge. Experimental tubes were connected to this vacuum system. A pressure of 0.1 micron was the lowest obtained during any of the experimental work.

Tin amalgam was utilized in the first attempt to form a cathode spot on a solid surface. Slepian[6] mentions that an ignitor rod cast into a tin surface would form a cathode spot even at atmospheric pressure. Preliminary tests showed that a 35 per cent tin amalgam was rigid, although what appeared to be a liquid phase could be shaken out of it. A d-c power source was connected to the tube containing tin amalgam, and a cathode spot started after a positive pulse was applied to the ignitor. The spot was self-maintaining and moved erratically over the solid cathode surface. The arc drop fluctuated rapidly within a range from 8 to 17 volts.

The cathode surface appeared bright and shiny where it had been traversed by the cathode spot. After a few minutes of operation the glass walls became covered

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J. A. M. LYON is associate professor of electrical engineering at Northwestern University, Evanston, Ill., and C. E. WILLIAMS is assistant research engineer, Standard Oil Company of Indiana. The experimental data in this paper have resulted from the research by C. E. Williams in partial fulfillment of the requirements for the degree of master of science from Northwestern University.

with an opaque, grayish deposit. This was probably tin sputtered from the cathode surface and oxidized partially by the leakage of air into the tube. The deposit would not melt at the softening point of Pyrex glass.

The next tube was built in a 500 cubic centimeter Pyrex flask and tested with a 50 per cent lead amalgam. The ignitor was a one-eighth inch carbon rod of approximately two ohms total resistance. A cathode spot could not be started, even with a 1,000-volt, 4-millicoulomb positive pulse applied to the ignitor. The ignitor resistance was evidently too low. A spot started readily when the terminal of a high frequency leak-tester was held against the glass. This tester would produce a low intensity one-inch spark in air. Data obtained with this tube are shown in Figure 2. This tube was also operated on 230 volts alternating current and conduction was started near the positive peak of each cycle by holding the leak tester against the glass. The arc drop appeared to vary inversely with current when viewed on a cathode-ray oscilloscope. This is in agreement with the d-c curve shown in Figure 2. The inner walls of this tube became coated with a bright film of sputtered metal. The thinner sections were semitransparent. The amalgam appeared bright where it had been subjected to a cathode spot. The film formation on the walls of the tin-amalgam and lead-amalgam tubes indicated that a considerable amount of material was being sputtered off the cathode. It is undesirable to have this depletion of the cathode since it would shorten the life of the tube.

A mercury-pool cathode ignitron, with a spacing of approximately $2^1/_2$ inches between anode and cathode, was built so that its arc drop could be compared with that of amalgam-cathode tubes. This tube had an arc drop of 15 volts while carrying a direct current of six amperes.

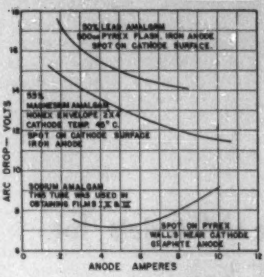

Figure 2. Arc drop of tubes with solid-amalgam cathodes

Experimental work was next conducted on sodium-amalgam tubes. There are several difficulties involved in preparing sodium amalgams. Sodium and mercury form definite compounds and considerable heat is evolved during amalgamation. If a piece of fresh sodium is removed from kerosene and dropped into mercury, a small explosion results. Probably the heat of formation ignites the kerosene. Mellor[3] states that the heat of formation of sodium amalgam is 20 calories per gram. Some amalgam mixed for use in a tube reached a temperature of over 200 degrees centigrade. The resulting mercury fumes are dangerous.

An attempt was made to form sodium amalgam by distilling mercury into sodium under vacuum. This failed when the glass chamber was broken while attempting to seal in the sodium. The method ultimately used was to cut the sodium into one-fourth inch cubes, wash them in water-free ether, and then drop them at intervals into a crucible of mercury. The amalgam sets immediately to a rigid mass. After cooling it is nearly as hard as lead and more brittle. This amalgam is readily oxidized and will decompose water slowly. This offers a ready means of cleaning it. The sodium breaks down the water and forms a solution of sodium hydroxide. The excess mercury forms a shiny protecting layer over the remaining amalgam. Samples of two per cent sodium amalgam left under water for 20 hours had apparently lost all of the sodium content. Sodium amalgam has a pronounced effect upon metals which are not ordinarily amalgamated by direct contact with mercury. Touching steel, nickel, Kovar, or aluminum to clean sodium amalgam will amalgamate them. However, the use of a stainless steel grid in a later tube indicated that sodium amalgam had no tendency to amalgamate with stainless steel. This means that tubes with stainless steel jackets could be utilized for tubes having sodium amalgam cathodes.

A sodium-amalgam tube was built in a 200 cubic centimeter Pyrex flask. The composition of the amalgam was approximately six per cent sodium and 94 per cent mercury. The amalgam was broken into small lumps with a hammer, and dropped into the tube. The rough pile of lumps shorted out the ignitor rod and conduction was started by holding the high-frequency leak-tester against the glass.

The arc-drop of the tube varied greatly with pressure. At 1 millimeter pressure the drop was 50 volts, while at 0.5 micron the arc drop fell as low as seven volts. The curves in Figure 3 show the characteristics of this tube. After operating a short time the walls of the flask became covered with a grayish deposit. The cathode spot left the cathode surface and traveled over this deposit. The spot cut a jagged path through the deposit and left only a thin gray film upon the glass. The spot glowed a deep yellow and heated the glass beneath it.

The spot traveled rapidly over the glass walls, ranging in position from the cathode surface up to any point within one-fourth inch of the anode lead. The amalgam lumps at the cathode did not melt at any time during the operation of this tube. At currents above five amperes two or more cathode spots formed on the wall at widely separated points. At twelve amperes four or more spots were erratically traveling about the inside of the tube. As a spot evaporated most of the coating from the glass, it traveled to a new and more thickly coated area. The tendency was for the spot to locate as near the anode as the thickness of the coating would permit.

This tube was sealed off the vacuum system at a pressure of 0.5 micron and performed without change when inverted.

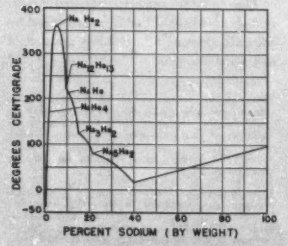

Figure 1. Freezing point curve of sodium-mercury amalgams after Mellor[3]

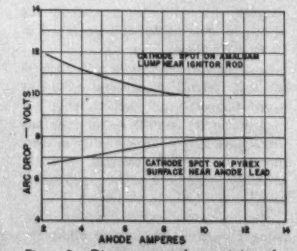

Figure 3. Direct-current characteristics of a sodium-amalgam tube

A drop of fluid amalgam condensed in the sealed-off tube. This could be rolled over the deposit on the glass walls without appreciably wetting it. This indicates clearly that the deposit was not composed solely of sodium and mercury. Apparently the hot sodium reacted with the Pyrex glass.

A new four per cent sodium amalgam was tested in a Pyrex bulb approximately two inches in diameter and five inches long. A one-inch diameter graphite anode was mounted axially within $1^1/_4$ inches of the cathode leads. This tube was similar to the one sketched in Figure 4. The d-c characteristics of this tube are shown in Figure 2.

The ignitor was a silicon-carbide rod approximately one-fourth inch in diameter. The amalgam lumps were melted in the bottom of the tube by holding a gas flame against the glass. The walls of this tube also became coated with a grayish deposit. All except a very thin film of this could be evaporated off with a Bunsen flame. The cathode spot generally did not travel more than two inches from the cathode surface. At this level it was approximately one-half inch from the anode. After completion of the oscillograms for several a-c tests, the tube was cut open and examined. The gray coating was approximately one-sixteenth inch thick and highly conductive. A resistance of less than one ohm was observed between points one inch apart on the coating.

The foregoing tube was operated on an a-c power supply with the anode firing with a delay of approximately 30 degrees. The ignitron pulse circuit of Figure 5 was used in the firing of this tube. Oscillograms were obtained for the performance of this tube, but are not reproduced in this paper. A single cycle of reverse conduction was observed on one oscillogram while operating from a 230 volt a-c line. The arc drop during the a-c tests of this tube was approximately 15 volts. This is roughly twice the d-c value shown in Figure 2 for this same tube. The d-c values were determined with the cathode spot on the side wall of the tube approximately one-half inch from the anode. When operated on alternating current, the spot was started at the cathode surface by the ignitor. For a-c operation, evidently, the mobility of the spot was not sufficient to permit it to move off the cathode surface during the time the anode was positive. This gave an arc length over twice as long as when the spot was on the glass side walls.

All the amalgam cathodes tested so far have shown no sign of melting during operation. This indicates that the per cent

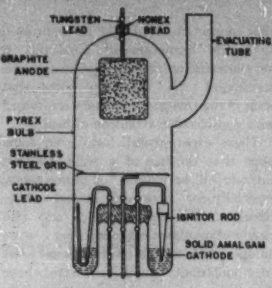

Figure 4. Experimental ignitron tube

of amalgamating metal could be reduced, which might cause the tube to perform as though it contained mercury alone. Another tube similar to the one sketched in Figure 4 was built up using a two per cent sodium amalgam. A stainless-steel grid, consisting of 0.136 inch holes drilled with a spacing of 0.176 inch between centers, was placed approximately one inch above the cathode surface. This was an attempt to reduce sputtering, as well as offering an opportunity of studying the distribution of charge at this point.

The waveform of grid current was observed on a cathode-ray oscilloscope while the anode carried a direct current of four amperes and seven volts alternating current was applied to the grid. The grid current appeared sinusoidal which indicates that there was a large supply of positive ions near the grid.

The grid seemingly had no effect on the amount of sputtering and maintaining it at the specified potential with respect to the cathode involved a considerable waste of power. The waveforms were very similar to those for the four per cent sodium-amalgam tube, except that ignitor currents were much greater and no reversed conduction occurred at any time. The ignitor in this tube was a boron-carbide rod.

Further tests of this tube were made with a 7.6 volt a-c potential applied to the grid in phase with the anode voltage. Electron current flows into the grid from the external circuit during anode conduction and oscillographic records show a grid current dip at the peak of its cycle. The arc drop of this tube was about 20 volts by d-c measurement. Most of the arc drop occurs near the cathode and causes the potential of the grid always to be greater than the applied 7.6 volt grid excitation. The difference will be least at the peak of the grid voltage and this would cause a decrease in grid current. This explanation accounts for the negative grid current and the dip near the peak of the grid current.

All of the tubes using sodium amalgams became blackened on the inside of the glass. Further effort is contemplated with sodium-resistant glass used commercially in sodium-vapor lighting equipment.

Magnesium amalgam was investigated to determine whether its higher melting point would make it less subject to sputtering. Magnesium does not react noticeably with mercury at room temperature. If the mercury is boiled in contact with magnesium chips, the amalgam forms in an explosive manner. The heat evolved may ignite part of the magnesium. The amalgam is very active. It decomposes water more rapidly than sodium amalgam and forms a black oxidized surface when exposed to air.

It was found that if the mercury and magnesium were placed in a tube and held under vacuum the amalgam formed slowly at room temperature. The heat evolved in the process of forming 100 grams of amalgam would keep the tube noticeably warm for 24 hours. The test data on a $5^1/_2$ per cent magnesium amalgam tube are plotted in Figure 2. The amalgam was very solid and appeared to have an excess of magnesium. Loose chips of magnesium lying on the cathode surface were blown about like dry leaves in a whirlwind by the vapor blast from the cathode spot. The tube walls did not become coated as rapidly or as completely as with the sodium amalgam. The cathode spot travelled over the walls near the cathode surface.

During the course of this work, it has been observed that all of the amalgams used oxidized readily. In all probability amalgamating the metals under vacuum would eliminate most of this difficulty; in future work, this will be a procedure to be tried. The experimental ignitron

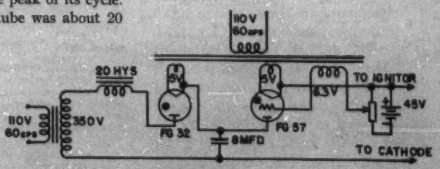

Figure 5. Ignitor pulse circuit

shown in Figure 4 could conceivably be damaged by the expansion or contraction of the amalgam during the operation of the tube. However, cracking of the glass from this cause was not observed. Some modification of the design of the tube will be attempted in further work.

None of the tubes tested were evacuated to the degree that is obtained in commercial tube manufacturing. It is possible that some of the arc-drop curves were influenced by the presence of residual gas within the tube. Regarding the inverse voltage breakdown strength of the tube, it was observed that only the sodium-amalgam tube with heavily sputtered walls ever failed at less than 330 volts inverse peak voltage.

No measurements were made of the temperatures of the cathode or the side walls of tubes. A spectrometer was not available for the work up to the present time. At an early date it is desired to make an analysis of the composition of the vapors present in such tubes by use of a spectrometer. This procedure will shed light on the energy transformations which take place within the tube. In this connection, it should be mentioned that the formation of amalgams is very interesting from the thermodynamic standpoint. It has been noted that some of the amalgams studied were formed with considerable evolution of heat.

These experimental tests give some hope that the use of a solid-amalgam cathode will be feasible for power tubes, especially for those used in low-voltage circuits. The controllability and efficiency of amalgam-cathode tubes is comparable with that of conventional mercury-pool tubes. There is reason to believe that with proper sodium-resistant glass the sodium-amalgam power tube can be perfected so as to give electrical performance comparable with mercury-pool tubes, and yet possessing the mechanical advantages desired, as stated at the beginning of this paper.

It should be pointed out that the tests studied so far indicated that they are not sensitive to changes in position, such as a complete inversion of the tube. Obviously, for long time operation, the cathode could not replenish itself in the inverted position.

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No Discussion

The General Solution Method of Power Network Analysis

LYLE A. DUNSTAN
MEMBER AIEE

Synopsis: The general solution method of power network analysis grew from the systematic investigation of the effects of changes in a power system being analyzed by mathematical methods. The investigation led to an improved method of computing load-flow studies by which successive approximations converging upon precise answers could be carried through without the solving of simultaneous equations, a method so flexible that changes in loads, ratios, phase shifts, or even the circuit offer no difficulties. The general solution, by taking advantage of a previously computed solution when computing a new solution, is particularly adapted to the computation of a family of solutions for a particular network. Investigation of the effects of changes indicated that the effect of a change in loading, transformer ratio, or phase shift upon any particular network answer could be closely approximated by multiplying the change by a coefficient. Similarly, the effect of a change in value of a circuit element, or the effect of a new circuit element added, upon any particular network answer could be approximated by multiplying a constant by a coefficient. This paper is concerned with the computation of the complete set of coefficients describing the effects of any loading, transformer ratio, phase shift, or circuit change upon any flow, voltage, or phase angle. The complete set of coefficients is termed in this paper the "general solution" to the network. Although the initial computation of the general solution for a large power system is a tedious process unless accomplished with punched-card machines or other automatic computing machinery, the application of the general solution, once computed, to innumerable system studies is extremely simple. The method makes possible the efficient handling of load-flow studies for power systems using no equipment other than a desk calculating machine. In addition to the facilitating of the making of load-flow studies, the general solution may serve to indicate the impedance between any two points in the system, either for fault purposes or for the determination of an "equivalent" for use in network analysis. Thus, the method should prove helpful for symmetrical components work. If general solutions have been computed for two adjacent systems, the effects of various interconnections between the systems may be readily evaluated.

General Flow Solution for Direct Current Problem

FOR illustrative purposes, the problem of determining the current in the simple network of resistances shown in Figure 1 is posed. This d-c problem is given as a first example because it is amenable to exact solution by classical methods, and because it illustrates the principles which are later applied to a-c power networks by suitable analogy and approximations. The problem illustrates how the general flow solution can simplify the computation of network current flows for any set of loads (currents supplied or withdrawn from the junctions), any voltage inserted at a particular point, a resistance changed to any desired value, and a new leg added to the network.

Consider the circuit shown in Figure 1, assuming the dotted circuit from A to D to be nonexistent, and the resistance of line AB to be 0.1 ohm. Initially, the problem is to determine the current flows throughout the network for any set of loads at points A, B, C, and D with the balance current made up at E, $E=-(A+B+C+D)$, and neglecting the effect of voltage V. Point E will be termed the "slack-take-up bus." The general solution to this problem is a set of coefficients to be multiplied by the loads, so that the sum of the products for each line is the line current flow. The set of coefficients for each load point is a set of distribution factors for a unit current supplied to the network at the load point and withdrawn at the slack-take-up bus. Although distribution factors may be computed either by star delta transformations or by simultaneous equations, such methods are relatively inefficient unless use is made of the computations for a unit current at one bus when computing for a unit current at another bus.

The circuit of Figure 1 is coded as shown in Figure 2 by assigning a positive direction of flow to each line and indicating the loops including each line. The circuit contains three loops, meshed so that no directional arrows conflict: loop 1 containing lines AB, BC, and CA; loop 2 containing lines BC, CD, and DB; and loop 3 containing lines AB, BC, CD, DE, and EA. Table I shows the lines in the circuit of Figures 1 and 2, the resistance of each line as taken from Figure 1, and the loops containing each line is taken from Figure 2. Since the summation of voltages about each loop must equal zero, simultaneous equations may be written, as shown in Table II, which illustrate that, if loop flows are taken as variables, the coefficients may be formed by summing the resistances by loops and by pairs of loops resulting in a symmetrical set. In Table II, ΣR_1 represents the summation of all resistances in loop 1, ΣR_{12} represents the summation of all resistances common to loops 1 and 2, and so for the other subscripts. I_1 represents current flow around loop 1, I_2 represents current around loop 2, and I_3 current around loop 3. Each equation contains a "K" term which is equal to all voltage in the loop not due to loop flows. By substitution of the values of resistance for each loop from Table I into the simultaneous equations of Table II, the simultaneous equations in the upper portion of Table III may be formed. For example, the I_2 coefficient in equation 2 (by symmetry this is also the I_2 coefficient in equation 3) is formed by adding all resistances from Table I which are in both loops 2 and 3, that is, 0.7, line BC, plus 0.4, line CD, equals 1.1.

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LYLE A. DUNSTAN is with the Federal Power Commission, Washington, D. C.

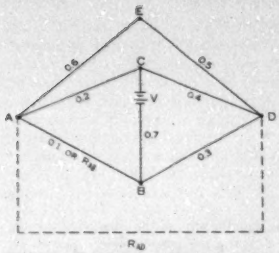


Figure 1. Circuit for d-c problem

The lower portion of Table III is a general solution for I_1, I_2, and I_3 in terms of k_1, k_2, and k_3. The set of coefficients of k_1, k_2, and k_3 is known mathematically as the inverted matrix of the set of coefficients of I_1, I_2, and I_3, and the Appendix gives a method of inverting a symmetrical matrix and a special type of nonsymmetrical matrix. The loop flows for any particular set of constants in the equations of Table III are equal to the sums of the products of the constants times factors from the inverted matrix, that is, I_1 equals $(-1.649\ k_1)$ plus $(0.599\ k_2)$ plus $(0.287\ k_3)$. Since currents supplied or withdrawn from the network at the junction points, and series voltages such as voltage V affect only the constant terms in the equations, the inverted matrix forms a basis for building a general solution for any current loads or series voltages.

Assume that a unit current flows from D to E in line DE and that circulating currents, of the proper magnitudes to balance voltage drops, flow around each of the three loops. Since line DE has a resistance of 0.5 ohm and is in loop 3 only, k_3 in the equations of Table III will equal 0.5 volts and k_1 and k_2 will equal zero. The loop flows for the condition equal k_3, or 0.5 times the k_3 column of the inverted matrix, or 0.1435, 0.2215, and -0.3735 for loops 1, 2, and 3, respectively. A unit flow through line CD, which has a resistance of 0.4 ohm and is in loops 2 and 3, will make k_2 equal k_3 equal 0.4 volts and k_1 equal zero, which will result in loop flows of 0.3544, -0.3676, and -0.1216 for loops 1, 2, and 3, respectively. If a unit current is supplied the network at D and withdrawn at E, the resulting currents may be assumed to be composed of components, a unit current through line DE, and loop circulating currents resulting from the unit current through line DE, as discussed earlier. If the unit current is supplied the network at C and withdrawn at E, the resulting currents may be assumed to be composed of components, a unit current through lines CD and DE, and loop circulating currents. The loop circulating currents for this case will equal the loop currents resulting from a unit current through line CD, plus those resulting from a unit current through line DE.

Table IV shows the computation of loop flows for a unit current supplied to the network at any bus and withdrawn at E. It may be noted from Figure 2 that all of the junction points (busses) in the network may be "scanned" by a single "track" starting at point A and proceeding to point E, the slack-take-up bus, as follows: point A, line AB, point B, line BC, point C, line CD, point D, and line DE. Table IV shows the lines and busses arranged in order of this track. The entries in this table for each line are the loop flows resulting from a unit current flowing through the line. These may be computed, as discussed in the previous paragraph for lines DE and CD, by multiplying the line resistance by the inverted matrix terms summed for the loops containing the particular line. The bus entries in Table IV are formed by taking progressive totals of the line entries starting at the slack-take-up bus and working up the track toward bus A. The bus entries are the loop flows which would result from a unit current supplied at the bus and withdrawn at the slack-take-up bus.

Table IV also shows the loop currents for a unit voltage inserted at V. Since V is in line BC which is in loops 1, 2, and 3, then k_1 equals k_2 equals k_3 equals V, and the entries in Table IV are formed by adding all three rows of the inverted matrix.

Having the loop flows, the knowing that the lines in the track between any particular bus and the slack-take-up bus must carry unit current for unit load at that bus, the entries in a general flow solution may be formed by combining components. The portion of Table V labeled "case 1" is the general flow solution. The line AB entry for load at A, 0.276, is equal to 1.000 plus (-0.1724) plus (-0.5530) since this line is in the track between current entry point and slack-take-up bus and is in loops 1 and 3.

The portion of Table V labeled case 1 enables line flows for any set of current loads and voltage V to be readily obtained. Loads are positive when current enters the network and negative when current is withdrawn, and voltage V is positive when it tends to force current from C to B in line CB. The currrent in each line is equal to the sum of the products of loads A, B, C, and D and voltage V times the proper coefficients from Table V. Thus, assuming specific loads $A=1$, $B=-2$, $C=-3$, and $D=5$ and voltage $V=0.1$, the flow from A to B would equal $(1)(0.276)+(-2)(-0.542)+(-3)(0.003)+(5)(-0.230)+(0.1)(-0.780)$, or 0.123, where the multiplying coefficients are taken from Table V. Other flows for this assumed loading are given in the answer portion of Table V.

The portion of Table V labeled "case 2" illustrates how the general solution may be used when the value of a circuit constant is changed. It is assumed that the resistance of line AB is changed from 0.1 ohm to R_{AB} ohms. This situation is handled by an extra current supplied at the sending end of line AB and withdrawn at the receiving end, the extra current being of such a value that the resulting current times the original AB resistance is equal to the resulting current less the extra current, times the desired resistance. Since the effect of an extra current supplied at A and withdrawn at B is equal to the extra current times (coefficient A less coefficient B) for any line, a row of coefficients may be computed by subtracting the bus B coefficients from the bus A coefficients in the general solution, Case 1. For line AB, coefficient A less coefficient B equals $0.276-(-0.542)$, or 0.818, and

Table I. Line and Loop Data for D-C Problem

Line	Resistance	Loops
AB	0.1	1 3
BC	0.7	1 2 3
CD	0.4	2 3
DE	0.5	3
CA	0.2	1
DB	0.3	2
EA	0.6	3

Table II. General Form of Simultaneous Equations

$$\Sigma R_1(I_1)+\Sigma R_{12}(I_2)+\Sigma R_{13}(I_3)+k_1=0$$
$$\Sigma R_{12}(I_1)+\Sigma R_2(I_2)+\Sigma R_{23}(I_3)+k_2=0$$
$$\Sigma R_{13}(I_1)+\Sigma R_{23}(I_2)+\Sigma R_3(I_3)+k_3=0$$

Table III. Simultaneous Equations and Inverted Matrix

$$1.000\ I_1+0.700\ I_2+0.800\ I_3+k_1=0 \quad \text{(Equation 1)}$$
$$0.700\ I_1+1.400\ I_2+1.100\ I_3+k_2=0 \quad \text{(Equation 2)}$$
$$0.800\ I_1+1.100\ I_2+2.300\ I_3+k_3=0 \quad \text{(Equation 3)}$$
$$-1.649\ k_1+0.599\ k_2+0.287\ k_3=I_1$$
$$0.599\ k_1-1.362\ k_2+0.443\ k_3=I_2$$
$$0.287\ k_1+0.443\ k_2-0.747\ k_3=I_3$$

Table IV. Loop Flows for Unit Loads

	Loop 1	Loop 2	Loop 3
A	−0.1724	−0.2659	−0.5530
AB	−0.1362	0.1042	−0.0460
B	−0.0362	−0.3701	−0.5070
BC	−0.5341	−0.2240	−0.0119
C	0.4979	−0.1461	−0.4951
CD	0.3544	−0.3676	−0.1216
D	0.1435	0.2215	−0.3735
DE	0.1435	0.2215	−0.3735
E	0.0000	0.0000	0.0000
V	−0.763	−0.320	−0.017

subtracting one to eliminate the extra current leaves -0.182, which is the entry for line AB in Case 2 in Table V. An equation may be set up involving the initial flow in line AB, the extra current, and the desired resistance from A to B, knowing that the initial resistance from A to B equals 0.1 ohm, as follows:

$$(F_{AB}+0.818EC)(0.1)=(F_{AB}-0.182EC)R_{AB}$$

where F_{AB} = initial flow from A to B, EC = extra current, and R_{AB} = new resistance from A to B. Solving for the extra current, EC, gives the multiplying factor listed for case 2 in Table V. Assuming the same loading and voltage V as for case 1, if resistance AB is changed from 0.1 ohm to 1.0 ohm, the extra current becomes 0.420 and the new current flow from A to B becomes $0.123+(0.420)\times(-0.182)$, or 0.047. Other flows for this condition are given in the answer portion of Table V.

R_{AB} could be infinite for line AB, an open circuit, and the multiplying factor would become $5.49F_{AB}$, which would give the proper currents throughout the network for this condition.

The portion of Table V labeled case 3 illustrates how the general solution may be used when a new circuit is added to the network. The new circuit to be added is the line from A to D, which is shown dotted in Figure 1. Assuming that unit current flows from A to D in the added leg, the effect on the network is the same as if a unit current were supplied at D and withdrawn at A, so that flow coefficients in Table V are formed by subtracting the bus A coefficients from the bus D coefficients. Thus the case 3 coefficient for line AB equals $-0.230+-0.276$, or -0.506. The actual flow in the added line from A to D will probably not be unity, but will be equal to the voltage around a loop divided by the resistance of the loop where the loop is formed by the added circuit and a path through the network, following the directional arrows, between the points of connection of the added circuit. The loop formed by the added leg consists of lines AD, DE, and EA, and the voltage around the loop equals $(-0.5)\times$ (flow DE) plus (-0.6)(flow EA) where the resistances of lines DE and EA are 0.5 and 0.6 ohms, respectively. The network resistance between points D and A is equal to the voltage drop between these points, due to a unit current supplied at D and withdrawn at A. From the known resistances of lines DE and EA and from the flow coefficients for Case 3 in Table V, the network resistance from D to A is equal to (0.5)(0.179) plus (0.6)(0.179), or 0.197 ohm. The current in the added circuit AD, which is the multiplier for case 3, equals

$$\left[\frac{-1}{R_{AD}+0.197}\right](0.5F_{DE}+0.6F_{EA})$$

Note that the new circuit added to the network could as well have been another network, and, having a general flow solution for each of the two networks, the flow coefficients and multiplying factor for interconnecting the two networks could be computed as discussed.

The changes in case 1 to produce case 2 and case 3 may be made simultaneously to produce case 4. Although the flow coefficients for case 4 are the same as those for case 2 and case 3 in Table V, the multiplying factors for this situation are mutually dependent and must be determined by solving two equations in two unknowns. If R_{AB} is assumed changed to 1.0 ohm, the multiplying factor for case 2 coefficients equals 3.412 (Flow $AB+\Delta AB$) where ΔAB is the change in flow AB, due to the case 3 change. Referring to Case 3 coefficients in Table V, it may be noted that ΔAB equals (-0.506)(case 3 multiplying factor). Similarly, if R_{AD} is assumed connected equal to 0.25 ohm, the multiplying factor for Case 3 may be expressed as equal to $(-2.237)(0.5DE+0.6EA+\Delta)$ where Δ equals $[(0.5)(-0.046)+(0.6)(-0.046)]$ (case 2 multiplying factor). Solving the equations for ΔAB and Δ simultaneously gives ΔAB equal to 0.534 and Δ equal to -0.114. The multiplying factors for case 2 and case 3 are then 2.242 and -1.055, respectively, assuming the same loads and voltage V as previously. Under this condition, the flow from A to B is $0.123+(2.242)(-0.182)+(-1.055)(-0.506)$, or 0.249. Other flows for this condition are given in the case 4 row of answers in Table V.

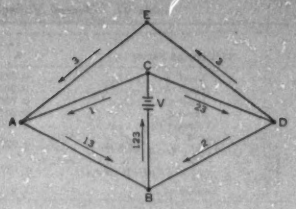


Figure 2. Coding for d-c problem

General Voltage Solution

Table VI is a general voltage solution to the circuit of Figure 1. The coefficients in this table may be multiplied by the same factors as for the general flow solution, and the products summed for each bus to give the bus voltage with respect to a ground or neutral voltage. The slack-take-up bus, E, is used as a reference, and the voltages at all other points are computed by summing the voltage drops in the track between E and each other bus, that is, voltage E is known, voltage D = voltage E plus drop DE, voltage C = voltage D plus drop CD. The voltage drop for each line is equal to the flow coefficient from Table V times the line resistance from Table I, so that the computation of the general voltage solution consists of multiplying all of the flow coefficients by line resistances and sum-

Table V. General Flow Solution for D-C Problem

Case	Multiplying Factor	Flow Coefficients							
		AB	BC	CD	DE	CA	DB	EA	AD
1	Load A	0.276	0.010	0.182	0.448	−0.172	−0.366	−0.552	
	Load B	−0.542	−0.088	0.124	0.494	−0.036	−0.370	−0.506	
	Load C	0.003	−0.143	0.359	0.505	0.498	−0.146	−0.495	
	Load D	−0.230	−0.008	−0.152	0.627	0.144	0.222	−0.373	
	Voltage V	−0.780	−1.100	−0.337	−0.017	−0.763	−0.320	−0.017	
2	$\left[\frac{R_{AB}-0.1}{0.182R_{AB}+0.0818}\right]$ (F_{AB})	−0.182	−0.078	0.058	−0.046	−0.136	0.104	−0.046	
3	$\left[\frac{-1}{R_{AD}+0.197}\right]$ $(0.5F_{DE}-0.6F_{EA})$	−0.506	−0.018	−0.334	0.179	0.316	0.483	0.179	1.000
					Answer Table				
1	For specific assumed loads and voltage V from text	0.123	0.113	−1.937	1.078	−0.950	1.990	0.078	
2	With R_{AB} changed to 1.0 ohm	0.047	0.080	−1.913	1.059	−1.007	2.034	0.059	
3	With R_{AD} = 0.25 ohm connected	0.786	0.137	−1.499	0.843	−1.354	1.350	−0.157	−1.311
4	Cases 2 and 3 simultaneously	0.249	−0.043	−1.455	0.786	−1.588	1.708	−0.224	−1.055

Table VI. General Voltage Solution for D-C Problem

Multiplying Factor	Voltage Coefficients A	B	C	D	E
Load A	0.331	0.304	0.297	0.224	0.000
Load B	0.304	0.358	0.297	0.247	0.000
Load C	0.296	0.296	0.396	0.253	0.000
Load D	0.224	0.247	0.253	0.314	0.000
Voltage V	0.009	0.087	−0.143	−0.009	0.000
$\left[\frac{R_{AB}-0.1}{0.182\,R_{AB}+0.0818}\right](F_{AB})$	0.027	−0.054	0.000	−0.023	0.000
$\left[\frac{-1}{R_{AD}+0.197}\right](0.5F_{DB}+0.6F_{EA})$	−0.107	−0.057	−0.044	0.090	0.000
Voltage at E	1.0	1.0	1.0	1.0	1.0

ming the products down the track, row for row. In the case of the voltage V row, it is important to include voltage V in the drop in line CB when summing the voltage drops down the track. For the case 2 row, when computing the voltage drop in line AB, it is important to use the original R_{AB}, or 0.1 ohm, and to increase the flow coefficient by unity, so that the voltage drop equals $(0.1)(1.000-0.182)$, or 0.0818 volts.

The reciprocal theorem is illustrated by the fact that the voltage coefficients for busses A, B, C, and D form a symmetrical array in Table VI.

The general voltage solution forms a convenient basis for computing the resistance between any two network points. For example, the resistance from A to D, which was previously computed to be 0.197 ohm, may be computed from the coefficients in Table VI for busses A and D for loads at A and D. Since the resistance from A to D equals voltage A minus voltage D, with a unit current supplied at A and withdrawn at D, the resistance equals $0.331-0.224-0.224+0.314$, or 0.197 ohm. Because of the symmetry of the voltage coefficients, the resistance may also be expressed $0.331+0.314-2\times(0.224)$, or 0.197 ohm.

Other General Solutions

A general flow and voltage solution may be prepared for a series voltage inserted in each line of the network. The computation of this solution is the same as the computation of the voltage V row in the previous example. Although this general solution has little significance in the d-c problem, it is useful in carrying out the steps in successive approximations in a-c networks. Note that, for the case of the series voltage, a unit drop, in addition to the resistance drop, for the particular line containing the series voltage, must be considered when computing the general voltage solution.

Another general solution which is frequently of use consists of a set of coefficients for each loop in the network, so that a constant may be inserted in any loop and all flows and voltages determined by multiplying the constant by the coefficients. The voltage would be in error for this solution unless the constant drop were located at a particular point, so that it could be added to all voltages in the track beyond that point.

Table VII. Line and Loop Data for A-C Problem

Line	Resistance	Reactance	Loops		
AB	0.09	0.14			
BC	0.07	0.12		3	
CD	0.02	0.05	2		
DE	0.12	0.14	2		
EC	0.08	0.16	1	2	
CF	0.10	0.18	1		3
FE	0.07	0.15	1		
FG	0.53	1.08			4
GH	0.00	0.11			4
HI	0.03	0.07			4
IJ	0.09	0.18			4
JF	0.00	0.11			4
FB	0.11	0.20		3	

Application to A-C Power Circuits

The d-c general solution for flows and voltages, which was based upon exact application of Kirchhoff's laws, may be extended to a-c networks by suitable approximations, and, if properly applied, will solve load-flow problems with great accuracy and speed. If, in the d-c problem, the resistances were replaced by reactances, current flows and loads by power flows and loads, and voltage V by a phase shifter with V radians of phase shift, the general flow solution would give, for an a-c circuit, the power flows for a given set of loads based upon the making of PX plus phase shift zero about each loop. The d-c general voltage solution would become, for the a-c case, a general solution for phase angles at each bus expressed in radians and based upon a known angle at the slack-take-up bus. Similarly, substituting reactive power for current in the d-c case, and transformer boost or buck for voltage V, would result in a general solution for reactive power flows and bus voltages based upon a known voltage at the slack-take-up bus.

Answers obtained from application of the general solution as discussed in the foregoing paragraph would be in error, due to the assumption of zero loss of power and reactive power and due to the assumption of phase shift equal to PX and voltage drop equal to QX. From the vector diagram shown in Figure 3, it may be noted that the IZ voltage and phase angle drops may be expressed respectively:

$$(E+PR/E+QX/E)\sec\theta-E \text{ and } \tan^{-1}(PX-QR)/(E^2+PR+QX)$$

The errors from the assumption of zero loss, and from rough approximation of voltage and phase angle drops, may be reduced by reapplying the answers to the general solution in a method of successive approximation.

Losses may be taken into account by computing the loss for each line from the approximate flow and voltage and then considering the loss as an additional load at the sending end bus, if receiving flows are desired. Sending and receiving ends of lines are based on the directional arrows in the initial coding rather than the actual direction of flow.

The difference between the phase angle drop and voltage drop based upon PX and QX, respectively, and the more accurate phase angle drop and voltage drop based upon the approximate flows and exact equations from the vector diagram in Figure 3, may be computed for each line by subtracting PX or QX, as the case may be, from the drop computed from the accurate equations. The difference thus obtained may be regarded as a series voltage or phase angle for each line, which may be multiplied by the coefficients in a general solution, computed for a series voltage in each line to obtain corrective terms to be added to original flows and voltages to obtain improved answers.

It is necessary to work in the per unit system so that voltages will be near unity, and angles should be in radians so that the difference between an angle and its tangent will be small. Charging current is considered as additional reactive load at

Table VIII. Form of Simultaneous Equations for A-C Problem

$$\Sigma X_1 P_1+\Sigma X_{12}P_2+\Sigma X_{13}P_3|-\Sigma R_1 Q_1-\Sigma R_{12}Q_2-\Sigma R_{13}Q_3+k_1=0$$
$$\Sigma X_{12}P_1+\Sigma X_2 P_2+\Sigma X_{23}P_3|-\Sigma R_{12}Q_1-\Sigma R_2 Q_2-\Sigma R_{23}Q_3+k_2=0$$
$$\Sigma X_{13}P_1+\Sigma X_{23}P_2+\Sigma X_3 P_3|-\Sigma R_{13}Q_1-\Sigma R_{23}Q_2-\Sigma R_3 Q_3+k_3=0$$
$$\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots$$
$$\Sigma R_1 P_1+\Sigma R_{12}P_2+\Sigma R_{13}P_3|+\Sigma X_1 Q_1+\Sigma X_{12}Q_2+\Sigma X_{13}Q_3+k_1=0$$
$$\Sigma R_{12}P_1+\Sigma R_2 P_2+\Sigma R_{23}P_3|+\Sigma X_{12}Q_1+\Sigma X_2 Q_2+\Sigma X_{23}Q_3+k_2=0$$
$$\Sigma R_{13}P_1+\Sigma R_{23}P_2+\Sigma R_3 P_3|+\Sigma X_{13}Q_1+\Sigma X_{23}Q_2+\Sigma X_3 Q_3+k_3=0$$

Table IX. Simultaneous Equation Coefficients and Inverted Matrix for A-C Problem

P_1	P_2	P_3	Q_1	Q_2	Q_3						
0.49	0.16	0.18	−0.35	−0.08	−0.10						
0.16	0.35	0.0	−0.08	−0.22	0.0						
0.18	0.0	0.50	−0.10	0.0	−0.28						
0.25	0.08	0.10	0.49	0.16	0.18						
0.08	0.22	0.0	0.16	0.35	0.0						
0.10	0.0	0.28	0.18	0.0	0.50						
0.49	0.16	0.18	(−0.25)	(−0.08)	(−0.10)	−0.3265	−0.3674	0.5102	0.1633	0.2041	−2.0408
	0.2978	−0.0588	(0.0016)	(−0.1939)	(0.0327)		0.1974	−0.0055	0.6511	−0.1097	−3.3584
		0.4223	(−0.0078)	(−0.0089)	(−0.2368)			0.0186	0.0211	0.5608	−2.3682
									−0.3237	−0.3814	−1.6189
			0.6177	0.1999	0.2356					0.1794	−2.3541
				0.4248	−0.0762						−1.8074
					0.5533						
−2.2146	0.9044	0.7943	1.1708	−0.5975	−0.4234 k_1						
0.9044	−2.4123	−0.3242	−0.5975	1.5827	0.2158 k_2						
0.7943	−0.3242	−1.8074	−0.4234	0.2158	1.0057 k_3	Inverted matrix for loops 1, 2, and 3					
−1.1708	0.5975	0.4234	−2.2146	0.9044	0.7943 k_4						
0.5975	−1.5827	−0.2158	0.9044	−2.4123	−0.3242 k_5						
0.4234	−0.2158	−1.0057	0.7943	−0.3242	−1.8074 k_6						
				P_4	Q_4						
				1.55	−0.65						
				0.65	1.55						
				1.55	(−0.65)	0.4194	−0.6452				
					1.8226		−0.5487				
				−0.5487	0.2301	Inverted matrix for loop 4					
				−0.2301	−0.5487						

the busses by using nominal *pi* representation for all lines.

General Solution Including Reactance and Resistance

Although the general solution based upon reactances only is a useful tool in network analysis, and can lead to exact answers by successive approximations, it is believed that a more elaborate general solution by which power, reactive power, resistance, and reactance are balanced simultaneously will prove to be more practical.

The expedient of balancing power and reactive power separately and alternately in a scheme of successive approximations was devised to avoid simultaneous equations in complex numbers, or to avoid having twice as many equations in twice as many unknowns. Since the general solution method involves simultaneous equations only in the initial computation of the coefficients, the economy of halving the number of equations at the expense of increasing the subsequent work to obtain answers becomes questionable. In many cases the accuracy of answers obtained from a single approximation using the general solution in resistance and reactance will be adequate.

Figure 4 is the impedance diagram of the high-voltage network of a utility company, Figure 5 shows the coding for this circuit, and Table VII gives the lines, resistances, reactances, and loops. Table VIII shows the form of equations to make the summation of $(PX-QR)$ and $(QX+PR)$ zero about loops simultaneously. In Table VIII, k_1, k_2, and k_3 represent phase angles not due to loop flows in loops 1, 2, and 3, respectively, and k_4, k_5, and k_6 represent voltages not due to loop flows in loops 1, 2, and 3, respectively. P_1 represents power circulating in loop 1 and Q_1 represents reactive power circulating in loop 1. The coefficients of the P and Q variables in the equations of Table VIII lack symmetry, due to the difference in signs of the resistance terms in the phase angle equations from those in the voltage equations. However, the coefficients in Table VIII may be divided by the dotted lines into four smaller sets, each of which is symmetrical.

Table IX shows the numerical values of coefficients in Table VIII, formed by summing the resistances and reactances from Table VII by loops, some numerical values arising in the matrix inversion (explained in the Appendix) and the inverted matrix. The inverted matrix is similar to the equation coefficients in that it lacks symmetry only by signs, and in that it may be divided into four smaller matrices, each of which is symmetrical. Although the network contains four loops, loop 4 does not mesh with loops 1, 2, and 3 and may be treated separately to avoid groups of zeros in a larger matrix. Table IX also shows the equation coefficients for loop 4 and the inverted matrix.

Having the inverted matrix, the loop flows for a unit load at each bus may be determined in a manner similar to the method used in the d-c example, busses and lines are arranged in tracks, line impedances are multiplied by inverted matrix terms, and bus terms are formed by taking progressive totals of the products. In the sample problem, bus F is the slack-take-up bus and all other busses may be scanned by two tracks: (1) A, AB, B, BC, C, CD, D, DE, E, EC, CF, and F; and (2) G, GH, H, HI, I, IJ, J, JF, and F. Table X lists the lines and busses in order of these two tracks. Consider a unit quantity of power entering the network, shown in Figure 5, at E and being withdrawn at F, the slack-take-up bus. This unit power may be assumed to flow through lines EC and CF, with the proper values of circulating power and reactive power to balance phase angles and voltages about the loops. This assumption of unit power flow through line EC, which is in loops 1 and 2, makes, in the equations

Table X. Computation of Loop Flows for Unit Bus Loads

	Q_1 / P_1	Q_2 / P_2	Q_3 / P_3	$-P_1$ / Q_1	$-P_2$ / Q_2	$-P_3$ / Q_3	*For Q Loads* / *For P Loads*	Q_4 / P_4	$-P_4$ / Q_4
A	−0.2055	−0.9114	−0.5279	−0.0028	−0.0075	−0.0023	*G*	−0.2856	0.042^2
AB	0.0000	0.0000	0.0000	0.0000	0.0000	0.0000	*GH*	−0.0604	0.025^3
AB	0.0000	0.0000	0.0000	0.0000	0.0000	0.0000	*GH*	0.0000	0.000^0
B	−0.2055	−0.9114	−0.5279	−0.0028	−0.0075	−0.0023	*H*	−0.2252	0.016^1
BC	0.0953	−0.0389	−0.2169	−0.0508	0.0259	0.1207	*HI*	−0.0384	0.016^1
BC	0.0296	−0.0151	−0.0704	0.0556	−0.0227	−0.1265	*HI*	−0.0060	-0.016^5
C	−0.3304	−0.8574	−0.2406	−0.0076	−0.0107	0.0035	*I*	−0.1799	0.0173
CD	0.0452	−0.1206	−0.0162	−0.0299	0.0791	0.0108	*IJ*	−0.0988	0.0414
CD	0.0120	−0.0317	−0.0043	0.0181	−0.0482	−0.0065	*IJ*	−0.0207	−0.0494
D	−0.3876	−0.7051	−0.2201	0.0042	−0.0416	−0.0008	*J*	−0.0604	0.0253
DE	0.1266	−0.3377	−0.0454	−0.0837	0.2216	0.0302	*JF*	−0.0604	0.0253
DE	−0.0717	−0.1899	−0.0259	0.1085	−0.2895	−0.0389	*JF*	0.0000	0.0000
E	−0.5859	−0.1775	−0.1488	−0.0206	0.0263	0.0079	*F*	0.0000	0.0000
EC	−0.2096	−0.2413	0.0752	0.0917	0.1576	−0.0332			
EC	−0.0459	−0.0788	0.0166	−0.1048	−0.1206	0.0376			
CF	−0.2557	0.1044	−0.1824	0.1345	−0.0687	0.1048			
CF	−0.0747	0.0382	−0.0582	−0.1420	0.0580	−0.1013			
F	0.0000	0.0000	0.0000	0.0000	0.0000	0.0000			

Table XI. General Flow Solution for A-C Problem

Load Point	Type Load*	Line Flow Coefficients															
		AB		BC		CD		DE		EC		CF		FE		FB	
		P	Q	P	Q	P	Q	P	Q	P	Q	P	Q	P	Q	P	Q
A	P	1.0	0.0	0.472	−0.002	0.089	−0.008	0.089	−0.008	−0.117	−0.010	0.267	−0.005	−0.206	−0.003	−0.528	−0.002
A	Q	0.0	1.0	0.002	0.472	0.008	0.089	0.008	0.089	0.010	−0.117	0.005	0.267	0.003	0.206	0.002	−0.528
B	P	0.0	0.0	0.472	−0.002	0.089	−0.008	0.089	−0.008	−0.117	−0.010	0.267	−0.005	−0.206	−0.003	−0.528	−0.002
B	Q	0.0	0.0	0.002	0.472	0.008	0.089	0.008	0.089	0.010	−0.117	0.005	0.267	0.003	−0.206	0.002	−0.528
C	P	0.0	0.0	−0.241	0.004	0.143	−0.011	0.143	−0.011	−0.188	−0.018	0.429	−0.004	−0.330	−0.008	−0.241	0.004
C	Q	0.0	0.0	−0.004	−0.241	0.011	0.143	0.011	0.143	0.018	−0.188	0.004	0.429	0.008	−0.330	−0.004	−0.241
D	P	0.0	0.0	−0.220	−0.001	−0.705	−0.042	0.295	−0.042	−0.093	−0.037	0.392	0.003	−0.388	0.004	−0.220	−0.001
D	Q	0.0	0.0	0.001	−0.220	0.042	−0.705	0.042	0.295	0.037	−0.093	−0.003	0.392	−0.004	−0.388	0.001	−0.220
E	P	0.0	0.0	−0.149	0.008	−0.178	0.026	−0.178	0.026	0.237	0.006	0.265	−0.013	−0.586	−0.021	−0.149	0.008
E	Q	0.0	0.0	−0.008	−0.149	−0.026	−0.871	−0.026	−0.178	−0.006	0.237	0.013	0.265	0.021	−0.586	−0.008	−0.149
		GH		HI		IJ		JF		FG							
G	P	0.714	0.042	0.714	0.042	0.714	0.042	0.714	0.042	−0.286	0.042						
G	Q	−0.042	0.714	−0.042	0.714	−0.042	0.714	−0.042	0.714	−0.042	−0.286						
H	P	−0.225	0.017	0.775	0.017	0.775	0.017	0.775	0.017	−0.225	0.017						
H	Q	−0.017	−0.225	−0.107	0.775	−0.017	0.775	−0.017	0.775	−0.017	−0.225						
I	P	−0.180	0.017	−0.180	0.017	0.820	0.017	0.820	0.017	−0.180	0.017						
I	Q	−0.017	−0.180	−0.017	−0.180	−0.017	0.820	−0.017	0.820	−0.017	−0.180						
J	P	−0.060	0.025	−0.060	0.025	−0.060	0.025	0.940	0.025	−0.060	0.025						
J	Q	−0.025	−0.060	−0.025	−0.060	−0.025	−0.060	−0.025	0.940	−0.025	−0.060						
GH	Ratio	−0.230	−0.549	−0.230	−0.549	−0.230	−0.549	−0.230	−0.549	−0.230	−0.549						
GH	Phase Shift	0.549	−0.230	0.549	−0.230	0.549	−0.230	0.549	−0.230	0.549	−0.230						

*Power or reactive supplied is positive load. Power or reactive withdrawn is negative load.

Table XII. General Voltage Solution for A-C Problem

Load Point	Type Load*	Voltage Coefficients					Phase Angle Coefficients
		A	B	C	D	E	E
A	P	0.1458	0.0586	0.0258	0.0244	0.0148	0.0306
A	Q	0.5155	0.1055	0.0487	0.0441	0.0306	−0.0148
B	P	0.0586	0.0586	0.0258	0.0244	0.0148	0.0306
B	Q	0.1055	0.1055	0.0487	0.0441	0.0306	−0.0148
C	P	0.0258	0.0258	0.0422	0.0399	0.0243	0.0490
C	Q	0.0484	0.0484	0.0776	0.0703	0.0490	−0.0243
D	P	0.0242	0.0242	0.0397	0.0559	0.0264	0.0583
D	Q	0.0439	0.0439	0.0703	0.1047	0.0583	−0.0264
E	P	0.0147	0.0147	0.0242	0.0264	0.0441	0.0864
E	Q	0.0306	0.0306	0.0490	0.0584	0.0864	−0.0441
		G	H	I	J		
G	P	0.1054	0.1008	0.0764	0.0046		
G	Q	0.3305	0.2520	0.2033	0.0785		
H	P	0.1010	0.0991	0.0747	0.0019		
H	Q	0.2522	0.2770	0.2232	0.0853		
I	P	0.0764	0.0745	0.0787	0.0019		
I	Q	0.2034	0.2232	0.3363	0.0002		
J	P	0.0046	0.0018	0.0018	0.0028		
J	Q	0.0788	0.0854	0.0904	0.1034		
GH	Ratio	0.7144	−0.2252	−0.1799	−0.0604		
GH	Phase Shift	0.0422	−0.0169	−0.0173	−0.0253		

*Power or reactive supplied is positive load. Power or reactive withdrawn is negative load.

Table XIII. Method of Inverting Symmetrical Matrix

	A	B	C	D	E	F	G
a	1.0	0.7	0.8	1.0			
b	0.7	1.4	1.1		1.0		
c	0.8	1.1	2.3			1.0	
d	1.000	0.700	0.800		−0.700	−0.800	−1.000
e		0.910	0.540			−0.593	−1.099
f			1.340				−0.746
g	−1.649	0.599	0.287				
h	0.599	−1.361	0.442				
i	0.287	0.442	−0.746				

Given: Aa, Ab, Ac, Ba, Bb, Bc, Ca, Cb, and Cc, where Ab = Ba, Ac = Ca, and so forth

Enter: Da = Eb = Fc = 1.0

Then: Da(Aa) = Ad; Da(Ba) = Bd; and Da(Ca) = Cd
−1/Ad = Gd
Gd(Bd) = Ed; Gd(Cd) = Fd
Eb(Bb) + Ed(Bd) = Be; Eb(Cb) + Ed(Cd) = Ce
−1/Be = Ge
Ge(Ce) = Fe
Fc(Ce) + Fd(Cd) + Fe(Ce) = Cf
−1/Cf = Gf

And: Gf = Ci
Ci(Fe) = Bi = Ch
Ci(Fd) + Bi(Ed) = Ai = Cg
Ge + Ch(Fe) = Bh
Ch(Fd) + Bh(Ed) = Ah = Bg
Gd + Cg(Fd) + Bg(Ed) = Ag

Check: Ag(Aa) + Bg(Ba) + Cg(Ca) = −1
Ag(Ab) + Bg(Bb) + Cg(Cb) = 0
Ag(Ac) + Bg(Bc) + Cg(Cc) = 0
Ah(Aa) + Bh(Ba) + Ch(Ca) = 0
Ah(Ab) + Bh(Bb) + Ch(Cb) = −1
Ah(Ac) + Bh(Bc) + Ch(Cc) = 0
Ai(Aa) + Bi(Ba) + Ci(Ca) = 0
Ai(Ab) + Bi(Bb) + Ci(Cb) = 0
Ai(Ac) + Bi(Bc) + Ci(Cc) = −1

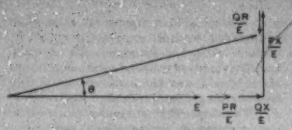


Figure 3. Vector diagram

$$\text{Voltage drop} = \left(E + \frac{PR}{E} + \frac{QX}{E}\right) \sec\theta - E$$

$$\text{Phase angle drop} = \tan^{-1} \frac{PX - QR}{E^2 + PR + QX}$$

shown in Table VIII, $k_1 = k_3 =$ the reactance of line EC, because k_1, k_2, and k_3 are phase shifts which are assumed equal to PX, and P is unity. Similarly, $k_4 = k_5 =$ the resistance of line EC. Thus, the entries in Table X for line EC are in two rows, one the product of X times the sum of the inverted matrix rows for k_1 and k_2, and the other the product of R times the sum of the inverted matrix rows for k_4 and k_5. For line CF, which is in loops 1 and 3, $k_1 = k_2 =$ reactance of line CF, and $k_4 = k_5 =$ resistance of line CF, and the entries in Table X for line CF are formed by products of the resistance and reactance by the proper rows of the inverted matrix. The loop flows for the unit power load entering the network at E equal the sums of the loop flows for unit power flow through lines EC and CF. The entries for other lines and busses in Table X are formed similarly.

Owing to the symmetry of the inverted matrix, except for the reversed signs, Table X may also be used for unit reactive loads. For this case, the power and reactive power about loops must be interchanged, and the power terms have the reversed sign.

The loop flows from Table X may be combined with the unit flows to obtain the general flow solution shown in Table XI. This table shows that the P and Q coefficients for any line are the same for P and Q loads, respectively, at any bus, and that the coefficients are the same except differing in sign for Q and P loads, respectively, at any bus. Since, with F as the slack-take-up bus, the effect of the loop 4 portion of the network is independent of the loops 1, 2, and 3 portion, the general flow solution is divided into two separate parts in Table X, to avoid the large groups of zeros that would result if the two parts were assembled into a single solution.

It is assumed that the transformer, GH, is capable of changing in ratio and in phase shift. The last two rows in Table X, which come directly from the inverted matrix for loop 4, may be multiplied by the transformer ratio (per unit boost in a direction opposite the arrow in Figure 5) and phase shift, expressed in radians, to show the effects upon the flow of power and reactive power in all lines.

Table XII is a general voltage solution prepared from the power and reactive power flows in Table XI and the line data in Table VII, assuming voltage drop in each line to equal $PR + QX$. A similar table for phase angles at each bus, based upon an angle of $PX - QR$ for each line, may be prepared, but, owing to the symmetry of the flow coefficients in Table XI, the angle coefficients are similar to the voltage coefficients, and may be obtained from the voltage coefficients by first reversing the sign of the P voltage coefficient and then interchanging the P and Q voltage coefficients. Phase angle coefficients are given in Table XII for bus E to illustrate this relationship.

The general voltage solution tables are symmetrical, that is, the load on bus A affects the voltage and angle of bus D in the same manner as load on bus D affects the voltage and angle at bus A.

The general voltage solution for the loop 4 portion of the network shows the effect of transformer ratio and phase shift on the various bus voltages. The coefficients for the transformer ratio include a unit voltage drop in line GH as well as the $PR + QX$ drops, analogous to the unit drop in the voltage solution for the series voltage in the d-c problem.

Conclusion

The general solution affords a convenient basis for computing voltages and current flows in a d-c network, and is especially useful for determining the effects of changing the circuit or one of the circuit constants.

The exact solution developed for direct currents may be extended to a-c power networks by certain approximations, and general solutions for power flow, reactive power flow, voltage, and phase angle in terms of loads, transformer ratios, and phase shifts may be prepared. It is believed that system engineers will quickly become familiar with the use of the general solution as shown in Tables XI and XII whether or not they study the method of preparing the solution, and that system engineers, including those having ready access to a network analyzer, will find it desirable to have a general solution to their system computed. Fortunately the computation of the general solution for a power network is not a difficult job.

Although the general solution may be used for computing complete system studies from basic data to final answers refined to any desired accuracy and such

Figure 4. Circuit for a-c problem

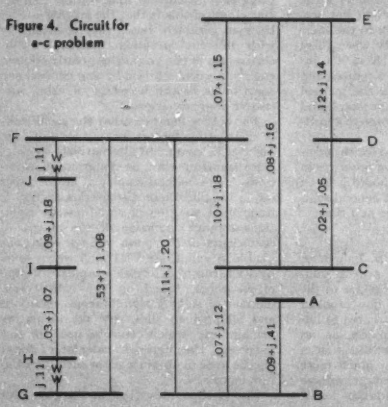
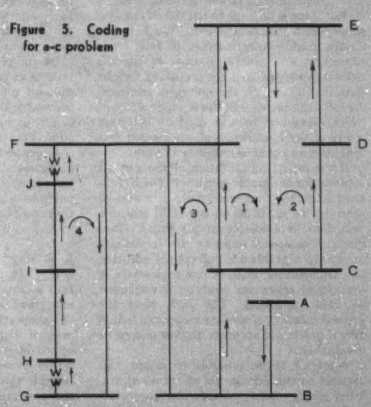


Figure 5. Coding for a-c problem



computing may require less time than a network analyzer study if punched-card equipment is used, the general solution is most practical for investigating the effects of slight changes to an existing system study. The existing study may have been computed, prepared from the readings from substation meters, or read from a network analyzer. Some types of changes may be computed more rapidly than they may be adjusted and read with a network analyzer, even if the network is set up and balanced at the time. Answers to frequently arising questions, such as the effect of an increased load on a flow or voltage at some point in the system, may be obtained very quickly. Referring to the circuit of Figure 4 and the general voltage solution in Table XII, suppose that it is desired to determine the amount of reactive power required at bus G to raise that bus voltage by five per cent, holding the slack-take-up bus at constant voltage. The answer is 0.05/-.3305, or 0.15 per unit vars. If it is desired to determine what change in phase shift of phase shifter GH will reduce the power flow in line HI by 0.1 per unit watt, the answer, from Table XI, is 57.3(0.1)/-0.549, or about $10^1/_2$ degrees. The 57.3 converts the phase shift from radians to degrees.

The methods of using the general solution to determine the impedance between network points, the effect of a change in a circuit constant, or the effect of interconnecting two networks is not given in detail in this paper for the a-c problem, but, by analogy to the d-c cases, it is apparent that similar mathematics will handle the a-c case. If desired, the general solution may be modified to give results directly in kilovolts, kilowatts, kilovars, and degrees instead of in per unit volts, watts, and vars, and in radians.

Appendix. Method of Inverting Symmetrical Matrix

Table XIII shows a method of inverting a symmetrical matrix which is quite practical for network analysis work. The method is well adapted to desk calculators since most of the steps involve the sums of two-factor products, and the desk calculator may accumulate the sums while the multiplications are being performed. Although the inverted matrix by this method has all signs reversed from the inverted matrix in mathematics, the reversed signs are beneficial if the equations are set up equal to zero, that is, with the "k" terms to the left of the equals sign.

In Table XIII a numerical example is worked out, and all steps are given in sequence below the example. The original matrix consists of rows a, b, and c of columns A, B, and C, and the inverted matrix consists of rows g, h, and i of columns A, B, and C. The steps may be readily followed, as each element in the numerical example is positively identified by a capital letter indicating the column and a lower case letter indicating the row. It may be noted that the numerical example is from the equations shown in Table III for the d-c problem. The steps illustrated in Table XIII follow a logical pattern, and it is believed that the reader will have no difficulty in extending the method to any number of variables.

Table IX shows a numerical example of the inversion of a 6-way matrix of the type arising in a-c network analysis. This matrix lacks symmetry in that the signs of the upper right fourth are reversed from the signs of the lower left fourth. However, the method of inverting the symmetrical matrix, shown in Table XIII, will also work for the special non-symmetrical matrix of Table IX provided that, when the solution has progressed through the row above the dashed line, the signs of all terms within the parentheses are reversed. Also, the signs of the upper right fourth of the inverted matrix should be reversed from the signs of the lower left fourth. Less than half of this special type of inverted matrix must be computed and the remainder may be filled in by symmetry.

Discussion

Glenn W. Bills (Bonneville Power Administration, Portland, Ore.). Dunstan's paper is an excellent contribution to system engineering. It is pleasing to see that he stresses his method of analysis rather than the novel use of an International Business Machine-type accounting machine, and that he has streamlined and improved the network solution which was presented in his first paper.

One distinct advantage of the general solution method of power network analysis is that there is no limit to the size of the systems which can be studied. We have recently doubled the number of line, load, capacitor, and generator units in our a-c network analyzer and are preparing for our first load study on the enlarged analyzer. Despite the increase in the number of board units, every unit is to be used on our next study of the Northwest interconnected system, and we could use additional units.

If the initial computation of the general solution for our power systems in the Northwest is not too time consuming, it would be desirable to set it up on our IBM machine and have it available at all times. Frequently someone requests an immediate answer to a problem involving the addition of a transformer, load, or line somewhere in our system when our analyzer is employed on other problems. For such cases the general solution, if already computed, could give a quick and accurate answer and so be invaluable.

Wouldn't it be possible to adapt this method to the calculation of transient stability swing curves, as only the generator-phase angles are changed for each step in the procedure, and all system impedances and generator and synchronous condenser voltages are fixed?

In many of our analyzer studies we resort to the use of equivalent networks to simplify board setups. These equivalents are either computed longhand or measured on the analyzer; Dunstan's method should enable us to readily obtain such equivalents in less time and with greater ease than has been possible in the past.

For large power systems where there may be as many as fifty loops, we wonder if the preliminary work of coding might not present an extremely difficult task to the average system engineer, especially when there exist complicated networks. Also, the inverting of matrices is usually a tedious and difficult undertaking although the method shown by Dunstan appears to be simple and can be performed without a thorough knowledge of matrix theory.

Dunstan is to be commended for the completeness of the two illustrative problems in his paper. These problems should help an engineer who is employing the general solution of a power network for the first time.

J. B. Ward (Purdue University, Lafayette, Ind.). In application of the general solution method described in this paper to the analytical solution of a power-flow problem the generation and loads are treated as impressed currents. This approach, as an approximation to actual conditions in a power network, is frequently much more fruitful than the use of constant voltage sources and constant impedance loads. This same approach together with the application of the superposition principle has been used in the development of various forms of a loss formula in which the total system loss is expressed in terms of the loading of generators and tie lines,[1,3] and in other papers on analytical methods of network solution.[2-6]

Power stations, through the action of voltage regulators and governors, usually maintain bus voltage and power loading constant, that is, in terms of classical linear network analysis they combine certain features of both current and voltage sources. One result of this is that the relative angles between generator buses vary considerably with different operating conditions. This elusiveness of the bus angles greatly complicates analytical solution by any method and contributes to the necessity of using successive approximations.

The author mentions that the coefficients or distribution factors for unit load at each bus can be computed alternatively by star-delta transformation or simultaneous equations. The method employed in the paper for evaluating these distribution factors is basically a solution of simultaneous equations through inversion of the matrix of coefficients of the mesh voltage equations (Table II and Table VIII). A recent book by Richardson[8] describes a tabular solution of networks, based on the substitution method of solving simultaneous equations and adapted to slide rule or calculating machine use, which could be used for this purpose. The network calculator might also be used to obtain a set of such distribution factors.

The methods described in the paper for

modifying a known solution to determine the effect of network changes, such as line outages, impedance changes, tap changes, etc., are very useful for obtaining additional information without the necessity of carrying through a new complete solution for each change. The superposition principles involved are not limited to an analytical solution but might also be applied to extend the life and usefulness of network calculator studies. Procedures of a similar nature based on superposition have been described by other authors,[4-6] and the references given here may be of interest to those who wish to pursue the study of these methods further.

The present paper describes a well-organized and coordinated method of obtaining and modifying a network solution. However, it appears that one definite limitation is the use of a single slack-take-up bus. In many high-voltage transmission studies voltage is held at a specified value, within reactive generating capabilities, at all generating stations and at numerous synchronous condensers in substations. In effect, this means that with respect to reactive power flow many buses act simultaneously as slack buses. The reactive power division among sources can vary widely with different line arrangements, even though the power generation schedule remains the same. For example, a change in tap setting can shift reactive power between sources with the real power loading held constant. This feature of many power network problems introduces serious complications for any analytical solution and requires cut and try procedures in calculator studies. It seems quite possible that in a system of this type analyzed by the method of the paper, considerably more than the one or two successive approximations mentioned by the author might be required in order to obtain with sufficient accuracy the desired solution.

It appears to the writer that examples of power networks for which the method of this paper seems particularly well adapted are 4 kv or 120/208 volt networks. These networks can usually be treated as single-source systems, conforming to the use of a single slack bus. The presence of many small loads and the wide range in impedance values of feeders, banks, and loads impose serious limitations on the solution of these networks by present day a-c network calculators. The use of business machines could relieve the burden of handling large numbers of branches. A general solution of the type described would be very useful for providing information relative to the addition of new banks or vaults, reinforcing secondary mains, and studying effects of large blocks of new load on secondary and bank loadings.

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6. Inverse Kirchhof Equations in Electric Networks, S. Matena. International Conference on Large High Voltage Systems (Paris, France) 1939, volume III, paper number 326.

Lyle A. Dunstan: The author is indebted to Bills and Ward for bringing forth ideas concerning the general solution for a power network.

Bills suggests the computation of a general solution for the Northwest interconnected system which might contain as many as fifty loops. The inversion of an n-way symmetrical matrix by the method given in the appendix involves n divisions and $1/2(n^2 - 1)(n + 2)$ multiplications. Thus if $n = 50$, 64,974 multiplications would be required, and if the solution were to be made to balance P and Q simultaneously, n would equal 100 and 509,949 multiplications would be required, a rather formidable task. However, the matrix would contain a large number of zeros since each loop would not mesh with each other loop. Further, it is probable that all 50 loops would not be interdependent so that the 50-way matrix would break down into several smaller matrices greatly simplifying the solution. For example, the a-c example in the paper, which was an actual power network, contained only four loops, but of these four, three were interdependent and one was independent of the other three.

The enlarged Bonneville Power Administration network analyzer contains 130 lines, 18 generators and 70 loads if data in the author's file are correct. If it is assumed that all of these analyzer units are connected into a circuit and that there is a bus for each generator and load, the number of loops in the network will equal one plus the excess of lines over busses or $1 + 130 - (70 + 18)$ or 43. Thus, a study of 50 loops is a large study in terms of the capability of modern large network analyzers.

Ward mentions that considerably more than two approximations might be required to obtain sufficient accuracy because of adjustments in reactive loading and transformer taps. It was not intended to imply in the paper that one or two approximations would settle reactive loading or transformer taps, but rather that, having the loading and transformer ratios, one or two approximations would give all network answers with sufficient accuracy.

The author fails to understand Ward's comment that the use of a single slack-take-up bus in the computation of the general solution is a disadvantage, since power and reactive power may be varied at any bus in the network with equal facility. Perhaps the term "slack-take-up bus," which was a carry-over from a previous paper, causes difficulty. This bus should be more properly referred to as "reference bus."

Ward's list of references should prove very helpful, especially the book by D. E. Richardson. Richardson, and William B. Roos, superintendent of the Laboratory Department of the Mexican Light and Power Company, Ltd., have called the author's attention to the fact that the inverse of the matrix of coefficients of the voltage drop equations corresponds to Richardson's "set of transfer conductances" (chapter VIII of his book).

A Practical Approach to Calculating Optimum Performance of Semiconductor Rectifiers

EARL D. WILSON
NONMEMBER AIEE

Synopsis: Applying Kirchoff's circuit laws to a conventional four-arm rectifier bridge, an expression is derived for efficiency in terms of R (effective reverse resistance of one arm), F (effective forward resistance of one arm), and L (ohmic load resistance). Knowing that R and F can be kept constant by separate adjustments of input voltage and temperature of the cells enables us to maximize efficiency, E, with respect to L. The optimum value of L turns out to be $\sqrt{RF}$, the geometrical mean of forward and reverse resistances. This value of L yields $E_{max} = [(q-1)/(q+1)]^2$, where $q = \sqrt{R/F}$. Further analysis shows that $\sqrt{R/F} = V_R/V_F$ for optimum L, where V_R and V_F are corresponding values of reverse and forward voltage drops, so that E_{max} is expressible in terms of V_R and V_F alone. Three families of curves are given to illustrate manipulation of the simple empirical data required, resulting in a final plot of E_{max} versus temperature for various current densities in the cells. An important feature of the process depends on the interesting analytical result that for maximum efficiency the reverse and forward losses are equal, making it possible to correlate values of V_R and V_F. Because the empirical data involved are strictly applicable only when R and F are not appreciably affected by previous values of V_F and V_R respectively, a modified technique is suggested in which data are observed only when optimum L is in the circuit. It is shown that under these conditions $E_{max} = (V_L/V)^2$ where V_L is the rms drop across L and V is the a-c input voltage. Interpretation of results is discussed.

WHEN a rectifier cell is functioning in a conventional circuit such as that diagrammed in Figure 1, it presents an effective resistance F to flow of current I_F in the forward direction and likewise an effective resistance R to flow of current R_R in the reverse direction. In the sketch, symbols are intended to illustrate conditions during a half cycle of input voltage V. Although F and R interchange each alternate half cycle, symmetry is preserved and analysis for any half cycle applies to continuous performance. For simplicity and clearness, analysis is confined to a single-phase circuit with a plain ohmic load resistance L. Furthermore, the rectifier elements will be considered on a unit area basis, effectively one cell per arm, each having one square inch of active area. Extension to multiple elements is straightforward.

Applying Kirchoff's current and voltage laws, the following basic equations are readily set up:

$$I = I_F + I_R \tag{1}$$

$$I_L = I_F - I_R \tag{2}$$

$$V = I_R R + I_F F \tag{3}$$

$$V = 2I_F F + I_L L \tag{4}$$

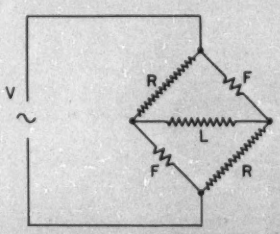

Figure 1. Diagram of conventional 4-arm rectifier bridge with symbols for effective reverse and forward resistances during a half cycle

where the only new symbols are I for the input current and I_L for the rms current through the load. From these equations it is not difficult to compute the rms wattage output as

$$W_o = \frac{(R-F)^2 L}{[(R+F)L+2RF]^2} \cdot V^2 \tag{5}$$

and the rms wattage input as

$$W_i = \frac{R+F+2L}{(R+F)L+2RF} \cdot V^2 \tag{6}$$

By definition, the efficiency of the rectifier is

$$E = \frac{W_o}{W_i} = \frac{(R-F)^2 L}{[(R+F)L+2RF][R+F+2L]} \tag{7}$$

Now R and F are not constant, being fundamentally individual functions both of applied voltage and temperature. Since L is a parameter independent of these two variables, it is possible to hold R and F fixed by adjusting input voltage and temperature while L is being changed. Hence it becomes permissible to maximize efficiency with respect to load resistance by differentiating E with respect to L and equating to zero. The optimum value of load resulting is

$$L_{opt} = \sqrt{RF} \tag{8}$$

In other words, the value of L to be used to get the highest E is simply the geometric mean of the effective reverse and forward resistances of the cell.

Substituting the optimum value of L into equation 7, the expression for maximum efficiency can be reduced to

$$E_{max} = \left(\frac{\sqrt{R/F}-1}{\sqrt{R/F}+1}\right)^2 = \left(\frac{q-1}{q+1}\right)^2 \tag{9}$$

where q is placed for $\sqrt{R/F}$.

For convenience in obtaining empirical data, it would be desirable to express

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EARL D. WILSON is with the Westinghouse Research Laboratories, East Pittsburgh, Pa.

Figure 2. Circuit diagrams showing how forward and reverse characteristics of rectifier cells may be measured

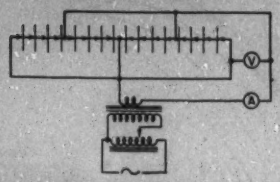

Figure 2A. How effective forward current-voltage characteristics of a 4-4-1 stack may be measured

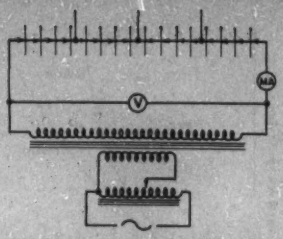

Figure 2B. How effective reverse voltage-current characteristics of a 4-4-1 stack may be measured

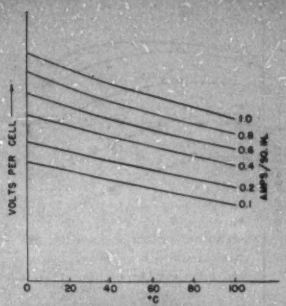

Figure 3. Plot of empirical forward data for rectifier cells relating volts per cell to temperature at various current densities

E_{max} in terms of V_R and V_F, the reverse and forward voltage drops across the cell. Fortunately it can be shown by use of equations 2, 3, and 4 that, allowing $L=\sqrt{RF}$,

$$q=\sqrt{R/F}=\frac{V_R}{V_F} \tag{10}$$

Thus it appears that maximum efficiency can be expressed in terms solely of V_R and V_F.

Of practical interest in industrial design of rectifier units is the maximum efficiency which can be obtained at various current densities and temperatures. Our analysis indicates that we need only obtain data on values of V_R and V_F within a practical range of operating conditions. In Figure 2 are circuit diagrams illustrating how such measurements may be made. A 4-4-1 stack of cells (a four-arm bridge with four cells in series in each arm and one in parallel) is connected as shown in (*A*) for observations on forward voltage-current characteristics. The instruments measure rms values. A four-junction thermocouple (not shown) is employed to give mean stack temperatures. For elevated temperatures, radiant heat from a drying lamp aids in maintaining a desired level, while colder temperatures can be held by drawing vapor from dry ice through a small blower.

In Figure 2 (*B*) is shown a variation of the circuit to obtain similar data in the reverse direction.

Figure 3 shows a family of curves resulting from the forward data reduced to a unit area basis. Figure 4 gives the curves reduced from the reverse data. Technically these are all we need.

It becomes necessary now to point out that in the ratio q, the values of V_R and V_F must be corresponding values; that is, they must in each instance be the effective values of reverse and forward voltages which exist together in adjacent arms of the bridge for a given current density and temperature. How to select corresponding values is not obvious, but fortunately a pursuance of our mathematical analysis points the way.

Returning to equation 10, we can square both sides and rearrange the terms to obtain

$$\frac{V_R^2}{R}=\frac{V_F^2}{F} \tag{11}$$

This is a remarkable conclusion. The term on the left represents reverse losses in the cell, while the term on the right represents forward losses. Equation 11 states that for optimum load resistance ($L_{opt}=\sqrt{RF}$), reverse and forward losses are equal. In addition, this yields a straightforward means of matching V_R to V_F. Since $V_R^2/R \equiv V_R I_R$ and $V_F^2/F \equiv V_F I_F$, we need only plot $V_R I_R$ versus V_R and $V_F I_F$ versus V_F for a given temperature and pick off V_R and V_F for the same wattage. Figure 5 shows such a plot for a temperature of 50 degrees centigrade. Similar curves are drawn for the range of temperatures involved. Then by applying equation 9 to these data we arrive at a family of curves which may be plotted as shown in Figure 6. Here we have arbitrarily plotted E_{max} as a function of temperature for various values of rms current density in the forward direction.

A glance at Figure 6 reveals that there is a narrow temperature range and a narrow current-density range in which there is a highest maximum efficiency. Presumably the parameters associated with this area are what the designer seeks. Such parameters are available from reference to the system of data. For example, what is the optimum value of L associated with highest efficiency at the optimum temperature and optimum forward current density? By manipulation of equations 8, 9, and 10,

$$L_{opt}=\frac{V_F}{I_F}\left(\frac{1+\sqrt{E}}{1-\sqrt{E}}\right) \tag{12}$$

Substituting E and I_F from Figure 6 and the corresponding value of V_F from the curves in Figure 3 for the optimum temperature, the best value of L_{opt} is computed. Likewise, a little algebraic juggling yields for the optimum input voltage,

$$V=\frac{2V_F}{1-\sqrt{E}} \tag{13}$$

Again, if we are more concerned about this optimum rms current in the load, we can find:

$$I_L=\frac{2\sqrt{E}}{1+\sqrt{E}}\cdot\frac{I_F}{V_F} \tag{14}$$

A corresponding approximate d-c average current could be found by dividing the value by an empirical form factor, usually about 1.13.

It would not be fair to conclude the analysis of the circuit at this point without recognizing the following experimental limitations in obtaining the data:

1. I_R is neglected when I_F is measured.
2. V_F is neglected when V_R is measured.
3. V_F is measured only when V_R is small.
4. V_R is measured only when I_F is small.

These facts merit a brief consideration. A study of the two circuits sketched in Figure 2 will show that reverse currents in one set of arms are included in the observed forward currents, and also that the forward voltage drops in one arm caused by reverse currents in the other arm are included in the observed reverse voltages. However, in both cases the errors are second order, and (if the procedure were judged advisable) could be reduced to third order values by estimated corrections. The preceding items 3 and 4 cannot be so easily dismissed. Experience has revealed that the forward drop of a

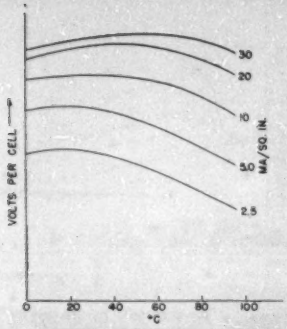

Figure 4. Plot of empirical reverse data for rectifier cells relating volts per cell to temperature at various current densities

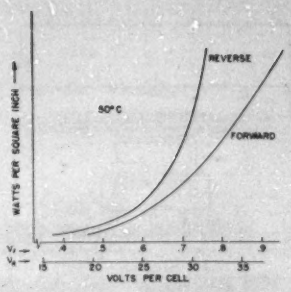

Figure 5. Plots of reverse and forward losses as functions of voltage at one temperature

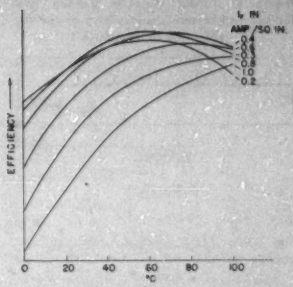

Figure 6. Plot of maximum efficiency as a function of temperature for various rms current densities

cell may be affected by the application of normal reverse voltage in the previous half cycle. Again the reverse leakage may be affected by the value of forward current density in the previous half cycle. The degree of the effects varies greatly among various types of cells. In some, they are negligible; in others, they are so serious as to make the described method of obtaining data quite inapplicable. The analysis, however, is still correct.

In view of this operational variable, we wish to outline a modified experimental procedure which eliminates the uncertainty in the data.

From the circuit in Figure 1 it is apparent that

$$V_R - V_F = V_L$$

and

$$V_R + V_F = V$$

Hence

$$\frac{V_R}{V_F} = \frac{V + V_L}{V - V_L} \tag{15}$$

When this relation is substituted into equations 10 and 9, we obtain simply

$$E_{max} = \left(\frac{V_L}{V}\right)^2 \tag{16}$$

Here E_{max} is given in terms of the ratio of rms load voltage to rms input voltage. Of course, this is true only for $L = L_{opt}$. Equation 16 suggests a procedure for observing data under actual conditions of dynamic operation of the bridge. Let the unit be held at some fixed temperature in a conventional circuit such that the values of input voltage V and load resistance L can be arbitrarily and independently adjusted until the value of the ratio V_L/V as indicated by an rms ratio voltmeter reaches a maximum value. The square of this observed datum gives E_{max} for the given temperature. The process is repeated for other temperatures in the range of interest. The curve of E versus temperature will exhibit a maximum which is the absolute value of the highest E_{max} for the cells measured. Empirical optimum values of V, V_L, and L could be easily recorded to advantage during the experimental procedure.

Such data should be free from the objections inherent in those obtained by the former method and should be completely applicable to any type of commercial or developmental rectifier cell.

Discussion

Stephen J. Angello (Westinghouse Research Laboratories, East Pittsburgh, Pa.): Performance analysis of nonlinear devices is essentially a difficult problem.

Wilson has replaced the nonlinear rectifier problem by a linear one, and has thereby arrived at a very useful guide for practical measurements.

There are limitations to this method in addition to those mentioned by the author. The choice of a bridge circuit eliminates tedious matching of boundary conditions at the half-cycle points provided that the rectifiers in the bridge arms are alike, or at least pairwise alike.

The equations one to four imply that the rms values of the fundamentals of the nonsinusoidal wave forms are of major importance and harmonics are neglected. The "effective" R and F are the resistances presented to fundamental currents.

An attempt to extend the theory to higher harmonics would spoil the beautiful simplicity of the method.

E. D. Wilson: The concept of replacing the nonlinear rectifier by a linear one did not originate with the author, but was suggested in a paper by P. O. Huss.[1]

Reference

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Analysis of Unsymmetrical Machines

KURT BURIAN
MEMBER AIEE

UNSYMMETRICAL 2-phase and single-phase induction machines, such as the capacitor and the split-phase motor with windings not in quadrature, can be analyzed by methods based either on the double-revolving field theory or on the cross-field theory of the single-phase motor. Whereas papers on the analysis of unsymmetrical machines based on the double-revolving field theory have been published,[1,2] no reference to an analysis based on the cross-field theory can be found. This is particularly regrettable in view of recent publications[3,4] which show the usefulness of the cross-field theory for the design of permanent split-phase motors.

It is the purpose of this paper to analyze the unsymmetrical machine from the viewpoint of the cross-field theory, and to derive an equivalent circuit for the machine.

In the following analysis only fundamental fluxes will be taken into account, the air gap will be assumed uniform, and the rotor will be assumed to have a symmetrical polyphase circuit of either the cage or the wound type. These assumptions are commonly made in induction motor theory.

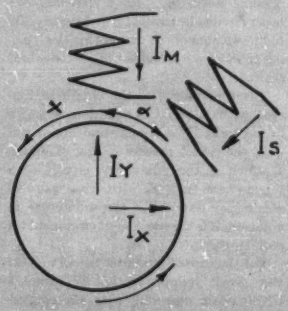

Figure 1. Unsymmetrical machine

Stator Magnetomotive Force

Let the auxiliary winding of the stator of an unsymmetrical machine be displaced from the main winding by α electrical radians, as shown diagrammatically in Figure 1. The magnetomotive force H set up by the two windings can then be expressed by

$$H = C[N_m I_m \sin x + N_s I_s \sin (x+\alpha)] \quad (1)$$

The reader is referred to the Table of Symbols for the meaning of the symbols in this and the following equations. Denoting the winding ratio N_s/N_m by a, equation 1 can be rewritten

$$H = CN_m(I_m + aI_s \cos \alpha) \sin x + CN_s I_s \sin \alpha \sin (x+90) \quad (2)$$

It is apparent from equation 2 that a magnetic field identical to that in the unsymmetrical machine exists in the air gap of a motor with the same main winding as that in the unsymmetrical machine, and with an auxiliary winding shifted relative to the main winding by 90 electrical radians provided that the current in the main winding equals

$$I_m' = I_m + aI_s \cos \alpha \quad (3)$$

and the current in the auxiliary winding equals

$$I_s' = I_s \sin \alpha \quad (4)$$

as shown diagrammatically in Figure 2.

Basic Equations of the Cross-Field Theory

The relations between the air-gap voltages and the rotor currents of a 2-phase or single-phase machine with windings in quadrature are well known. In the notation of this paper where these quantities are expressed in main-winding terms, the equations linking the components of the air gap voltage and the rotor currents in the x and y direction are

$$Z_s I_x = E_x - jSE_y - SX_s I_y \quad (5)$$

$$Z_s I_y = E_y + jSE_x + SX_s I_x \quad (6)$$

Furthermore, the components of the air gap voltage in any direction can be expressed by the product of the magnetizing impedance and the components of all the currents in that direction.

Thus, the components of the air gap voltage are

$$E_x = Z_m \sum_n I_{xn} \quad (7)$$

$$E_y = Z_m \sum_n I_{yn} \quad (8)$$

Finally, the voltage applied to any winding must be balanced by the sum of all induced voltages in the winding and the voltage drop due to the local impedance of the winding. This can be expressed generally by

$$V = \sum_n (E_n) + ZI \quad (9)$$

Equations 5 to 9 are the well-known relations derived from an analysis based on the cross-field theory.

Basic Equations of the Unsymmetrical Machine

Equations 5 to 9 can be readily applied to the motor of Figure 2:

$$Z_s I_x = E_x - jSE_y - SX_s I_y \quad (10)$$

$$Z_s I_y = E_y + jSE_x + SX_s I_x \quad (11)$$

$$E_x = Z_m(aI_s' - I_x) \quad (12)$$

$$E_y = Z_m(I_m' - I_y) \quad (13)$$

It should be noted that the auxiliary current I_s' in equation 12 was multiplied with a to reduce it to main winding terms.

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KURT BURIAN is in the engineering department of G-M Laboratories, Chicago, Ill.

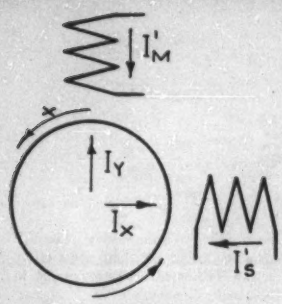


Figure 2. Equivalent machine with windings in quadrature

since all the other quantities involved are also in main-winding terms.

Applying equation 10 to both the main and auxiliary winding it follows that

$$V_m = Z_{1m} I_m + E_y \doteq j\omega M I_s \tag{14}$$

$$V_s = Z_{1s} I_s + E_s' \doteq j\omega M I_m \tag{15}$$

The term $j\omega M I_m$ was added to equation 15 to account for the voltage induced in the auxiliary winding by the local mutual leakage flux which links both the main and auxiliary winding of the unsymmetrical machine, but which does not cross the air gap. For the same reason the term $j\omega M I_s$ was added to equation 14. It should be noted that the local impedances Z_{1m} and Z_{1s} are defined here to include any circuit element used externally in series with the winding.

The air gap voltage E_s' can be found by taking the components of the voltages E_x and E_y in the direction of the auxiliary winding:

$$E_s' = aE_y \cos\alpha + aE_x \sin\alpha \tag{16}$$

where the factor a was used to reduce the voltages to auxiliary winding terms.

Substituting equations 3, 4, and 16 into equations 10 to 15, the following set of six equations results:

$$V_m = Z_{1m} I_m + E_y \doteq j\omega M I_s \tag{17}$$

$$V_s = Z_{1s} I_s + aE_y \cos\alpha + aE_x \sin\alpha \doteq j\omega M I_m \tag{18}$$

$$E_y = Z_m (aI_s \sin\alpha - I_y) \tag{19}$$

$$E_y = Z_m (I_m + aI_s \cos\alpha - I_y) \tag{20}$$

$$Z_s I_x = E_x - jSE_y - SX_s I_y \tag{21}$$

$$Z_s I_y = E_y + jSE_x + SX_s I_x \tag{22}$$

These six equations can be solved for the unknown currents I_m, I_s, I_x, and I_y and for the air gap voltages E_x and E_y. These quantities determine uniquely the performance of the unsymmetrical machine.

Torque Equation

Torque in an induction machine results from the interaction of the rotor currents with the air gap flux. Since the rotor currents and the air gap flux are the same for the motor of Figure 2 as they are for the unsymmetrical machine, and since the torque of the motor of Figure 2 is well known, the torque for the unsymmetrical machine follows immediately

$$T = |E_y|\,|I_x| \sin(E_y, I_x) + |E_x|\,|I_y| \sin(I_y, E_x) \tag{23}$$

It should be noted that the winding ratio does not appear in this equation, since by notation of this paper all quantities in this equation are in main winding terms.

Single-Phase Motor

For the case of a single-phase motor with windings not in quadrature, the voltages V_m and V_s are equal to the line voltage V. Furthermore, the coefficient of mutual inductance M is usually small, so that the terms involving M can be neglected. Under these conditions equations 17 and 18 reduce to

$$V = Z_{1m} I_m + E_y \tag{17a}$$

$$V = Z_{1s} I_s + aE_y \cos\alpha + aE_x \sin\alpha \tag{18a}$$

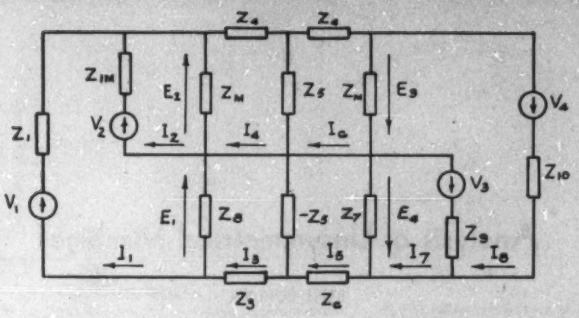


Figure 3. Equivalent circuit of the unsymmetrical machine

$V_1 = V/(a\cos\alpha)$	$I_1 = aI_s \cos\alpha$
$V_2 = V$	$I_3 = I_m + I_1$
$V_3 = jV\cot\alpha$	$I_5 = I_x \cot\alpha$
$V_4 = jV/(a\sin\alpha)$	$I_4 = I_y$
$E_1 = E_x \tan\alpha$	$I_3 = jI_y \tan\alpha$
$E_4 = E_y$	$I_8 = jI_x$
$E_3 = jE_x$	$I_7 = jI_m \tan\alpha + I_s$
$E_6 = jE_y \cot\alpha$	$I_6 = jaI_s \sin\alpha$
$Z_1 = Z_{1s}/(a^2\cos^2\alpha)$	$Z_7 = Z_m \cot^2\alpha$
$Z_3 = (Z_3' + R)\tan^2\alpha + R$	$Z_8 = Z_m \tan^2\alpha$
$Z_4 = Z_3'$	$Z_9 = Z_{1m}\cot^2\alpha$
$Z_5 = R$	$Z_{10} = Z_{1s}/(a^2\sin^2\alpha)$
$Z_6 = (Z_3' + R)\cot^2\alpha + R$	

Equivalent Circuit for the Single-Phase Motor

The set of six equations 17a to 22 can be used to design an equivalent circuit for the unsymmetrical machine. The solution of the problem of finding the equivalent circuit is not unique, and the circuit shown in Figure 3 is merely one of many possible solutions. Its preference over other circuits lies in that it reduces readily to known equivalent circuits for the limiting cases of $\alpha = 0$ and $\alpha = 90$ electrical radians.

The equivalence of the circuit of Figure 3 with the set of six equations is shown in Appendix III. For the limiting case of $\alpha = 0$ the circuit reduces to that of Figure 4 which is the equivalent circuit of a single-phase motor with two parallel windings. Except for the differences in notation, it is the equivalent circuit shown in reference 1.

For the case of $\alpha = 90$ electrical radians the circuit reduces to that of Figure 5 which again coincides with the equivalent circuit of reference 1.

Table I

	α = 60 Degrees Puchstein Lloyd	α = 60 Degrees Cross-field	α = 90 Degrees Puchstein Lloyd	α = 90 Degrees Cross-field	α = 120 Degrees Puchstein Lloyd	α = 120 Degrees Cross-field	α = 105 Degrees Lyon Kingsley	α = 105 Degrees Cross-field
I_m	13.35	13.40	14.2	14.15	15.64	15.68	14.85	14.85
I_s	4.26	4.29	6.28	6.31	8.54	8.58	7.41	7.40
I_x	14.56	14.62	17.5	17.6	21.3	21.3	19.31	19.30
T	446	453	760	756	908	913	860	860

Conclusions

Performance of the unsymmetrical induction machine can be predicted from design values of the motor by relations derived from the cross-field theory. The correctness of these relations is borne out by calculations on a sample motor the performance of which is given in references 1 and 2. Details of the calculations are given in Appendix II. The equivalent circuit developed from the basic equations lends itself to rapid solution by use of tabular methods such as are given in reference 5. Furthermore, the equivalent circuit can be simplified in particular cases by carefully chosen approximations.

Appendix I. Starting Conditions of Single-Phase Motor

For a single-phase motor at standstill ($S=0$), equations 17a to 22 can be readily solved for the unknown currents:

$$I_m = V(Z_{1s}+a^2Q-aQ\cos\alpha)/D \tag{24}$$

$$I_s = V(Z_{1m}+Q-aQ\cos\alpha)/D \tag{25}$$

where

$$Q = Z_3Z_m/(Z_3+Z_m) \tag{26}$$

$$D = (Z_{1m}+Q)(Z_{1s}+a^2Q)-a^2Q^2\cos^2\alpha \tag{27}$$

With the currents I_m and I_s known, the starting torque can be found from

$$T = 2aR_3|P|^2|I_m|\ |I_s|\sin\alpha\sin\varphi \tag{28}$$

where

$$P = Z_m/(Z_3+Z_m) \tag{29}$$

and φ is the angle between the vectors I_m and I_s.

Appendix II. Numerical Example

To illustrate the application of the equations given in Appendix I an example of a motor for which constants are given in references 1 and 2, is worked out here. Expressed in the notation of this paper the design constants of the motor are

$$Z_{1m} = 2.02+j2.79$$

$$Z_{1s} = (1.18)^2\times 5.12+3.0+j(1.18)^2\times 2.31-j14.5 = 10.13-j11.28$$

$$Z_3 = 2\times 2.06+j2\times 1.06 = 4.12+j2.12$$

$$Z_m = 2\times j33.4 = j66.8$$

$$a = 1.18$$

$$V = 110$$

From the manner in which the rotor and magnetizing impedances are defined here, these values are twice the corresponding impedances in reference 1. Using equations 24, 25, and 28, values for the currents and torque at various angles α are obtained. These values are contrasted in Table I with the values given in references 1 and 2.

Appendix III. Equivalent Circuit

To show the equivalence of the circuit of Figure 3 with the set of six equations 17a to 22, it is convenient to rearrange equations 21 and 22 as follows:

$$E_x = (Z_3'+R)I_x+jRI_y \tag{21a}$$

$$E_y = (Z_3'+R)I_y-jRI_x \tag{22a}$$

where

$$Z_3' = R_3/(1+S)+jX_3 \tag{30}$$

$$R = SR_3/(1-S^2) \tag{31}$$

Checking first the induced voltages it follows from Figure 3 that

$$E_1 = Z_5(I_1-I_5)$$

Substituting the values from Figure 3 it follows that

$$E_x\tan\alpha = Z_m\tan^2\alpha(aI_s\cos\alpha-I_x\cot\alpha)$$
$$E_x = Z_m(aI_s\sin\alpha-I_x)$$

which is identical to equation 19. Similarly it follows from Figure 3

$$E_3 = Z_mI(_3-I_4)$$
$$E_y = Z_m(I_m+aI_s\cos\alpha-I_y)$$

which is identical to equation 20. Furthermore

$$E_3 = Z_m(I_3-I_4)$$
$$jE_x = Z_m(jaI_s\sin\alpha-jI_x)$$

which again reduces to equation 19, and finally

$$E_4 = Z_7(I_7-I_4)$$
$$jE_y\cot\alpha = Z_m\cot^2\alpha(jI_m\tan\alpha+jaI_s\sin\alpha-jI_y\tan\alpha)$$
$$E_y = Z_m(I_m+aI_s\cos\alpha-I_y)$$

which again is identical to equation 20.

Applying Kirchhoff's law to the outer contour of meshes one and two, it follows that

$$V_1 = Z_1I_1+E_1+E_2$$

$$V/a\cos\alpha = aI_s\cos\alpha Z_{1s}/(a^2\cos^2\alpha)+E_x\tan\alpha+E_y$$
$$V = I_sZ_{1s}+aE_x\sin\alpha+aE_y\cos\alpha$$

which is identical to equation 18a. For mesh two it follows that

$$V_2 = Z_{1m}(I_2-I_1)+E_2$$
$$V = Z_{1m}I_m+E_y$$

which is identical to equation 17a. For mesh three it follows that

$$E_1 = I_5(Z_5-Z_3)+I_3Z_3$$
$$E_x\tan\alpha = I_x\cot\alpha(Z_3'+R)\tan^2\alpha+jRI_y\tan\alpha$$

which readily reduces to equation 21a. For mesh four it follows that

$$E_3 = I_4(Z_4+Z_3)-I_3Z_3$$
$$E_y = I_y(Z_3'+R)-jRI_x$$

which is identical to equation 22a. For mesh five it follows that

$$E_4 = I_5(Z_5-Z_3)+I_3Z_3$$
$$jE_y\cot\alpha = jI_y\tan\alpha(Z_3'+R)\cot^2\alpha+I_xR\cot\alpha$$

which again reduces to equation 22a. For mesh six it follows that

$$E_3 = I_6(Z_4+Z_3)-I_4Z_3$$
$$jE_x = jI_x(Z_3'+R)-RI_y$$

which again reduces to equation 21a. For mesh seven it follows that

$$V_3 = Z_8(I_7-I_6)+E_4$$
$$jV\cot\alpha = Z_{1m}\cot^2\alpha jI_m\tan\alpha+jE_y\cot\alpha$$

which reduces to equation 17a. And finally for the outer contour of meshes seven and eight it follows that

$$V_4 = I_8Z_{10}+E_3+E_4$$
$$jV/a\sin\alpha = jaI_s\sin\alpha Z_{1s}/(a^2\sin^2\alpha)+jE_x+jE_y\cot\alpha$$

which readily reduces to equation 18a.

Table of Symbols

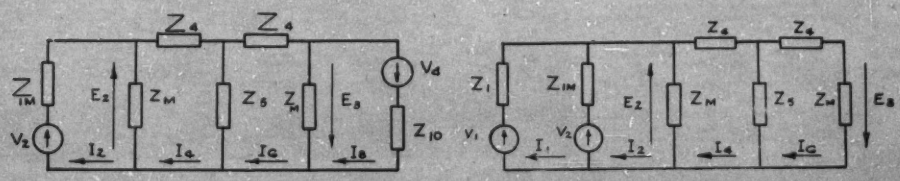
$a = N_s/N_m$ winding ratio
E_x = air gap voltage in x direction, in main winding terms, in volts
E_y = air gap voltage in y direction, in main winding terms in volts
H = magnetomotive force
I_m = current in main winding, in amperes
I_s = current in auxiliary winding, in ampere s

Figure 4. Equivalent circuit for $\alpha=0$

$V_1 = V/a$ $\quad E_2 = E_y$ $\quad I_1 = aI_s$ $\quad I_4 = I_y$
$V_3 = V$ $\quad E_3 = jE_x$ $\quad I_3 = I_m+I_1$ $\quad I_6 = jI_x$
$Z_1 = Z_{1s}/a^2$ $\quad Z_4 = Z_3'$ $\quad Z_5 = R$

Figure 5. Equivalent circuit for $\alpha=90$ degrees

$V_3 = V$ $\quad E_2 = E_y$ $\quad I_3 = I_m$ $\quad I_4 = jI_x$
$V_4 = jV/a$ $\quad E_3 = jE_x$ $\quad I_4 = I_y$ $\quad I_5 = jaI_s$
$Z_4 = Z_3'$ $\quad Z_5 = R$ $\quad Z_{10} = Z_{1s}/a^2$



$I_t = I_m + I_a$ total current, in amperes
I_x = rotor current in x direction, in main winding terms, in amperes
I_y = rotor current in y direction, in main winding terms, in amperes
M = coefficient of mutual inductance of main and auxiliary winding
N_m = effective turns of main winding
N_a = effective turns of auxiliary winding
R_2 = resistance of rotor in main winding terms, in ohms
S = ratio of actual motor speed to synchronous speed
T = torque in synchronous watts
V = line voltage
X_2 = inductance of rotor in main winding terms in ohms
Z_m = magnetizing impedance in main winding terms, in ohms
Z_{1m} = local impedance of main winding (including external impedance, if used), in ohms
Z_{1a} = local impedance of auxiliary winding (including external impedance, if used), in ohms
$Z_2 = R_2 + jX_2$ rotor impedance in main winding terms, in ohms

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No Discussion

Oscillographic Study of Synchronous Motor Hunting

E. B. KURTZ
FELLOW AIEE

ALL synchronous machines are subject to hunting. When a synchronous motor operates under constant load and excitation, there is a definite phase angle between the impressed and the excitation voltage. If the load is increased the motor will momentarily be developing less power than is required by the load and it will begin to slow down to assume the phase position corresponding to the increased load. However, the motor cannot immediately take up the new phase position corresponding to the increased load because the inertia of the rotor will cause it to overrun this position. It will then be developing more power than is required and cause the rotor to speed up and overrun the new phase position in the other direction. The rotor will continue this hunting action until the oscillations are damped out by the damper-winding action. A similar sequence of oscillations takes place when load is suddenly removed.

When the motor hunts it causes all the electrical quantities to vary; the phase angle between the impressed and the excitation voltages changes and causes the line current to change; the power taken by the motor varies as the rotating parts give up and absorb kinetic energy; and voltages are induced in the field windings when there is relative motion between the rotating field and the field structure. The exact variation of these quantities is important but difficult to predict analytically. Hence, this paper which presents an oscillographic study of a synchronous motor subjected to sudden load changes.

Apparatus and Connections

The apparatus used in this investigation is shown in Figure 1 and the connection diagram is given in Figure 2. As shown, the laboratory setup comprises a synchronous motor, a d-c generator, a power-angle indicator, and a 6-element oscillograph for recording the transient electrical quantities.

The motor is rated 3-phase 5-kva 220-volts 60-cycles 6-pole 1,200-rpm. It was designed for laboratory use, and hence is probably rated lower than a commercial machine of the same size. Its rotor was equipped with a squirrel cage winding. Its windings were Y-connected and had a neutral lead which was used in measuring and photographing the electrical phase quantities.

The load was applied or removed by closing or opening the resistance-load switch, S, of the directly coupled d-c shunt generator.

The power angle indicator, Figure 3, consists of two parts; one a disk mounted on the synchronous motor shaft, and the other, a brush arm fastened to the shaft of a small auxiliary synchronous motor having the same synchronous speed as the main motor. On the periphery of the disk is mounted an insulated high resistance wire through which a constant direct current is caused to flow. This current is supplied through slip rings from a battery. The brush arm is of such length that it rides freely on the high resistance wire. When load is applied to the main motor, the main motor will fall behind the idly running auxiliary motor and the brush will move around on the wire a small distance. If the motors are initially brought into step in the correct position, the voltage drop between the brush and the voltage drop lead will be proportional to the power angle. After the two motors are in correct relative positions, the voltage drop thus becomes a direct and instan-

Figure 1. Laboratory set-up showing synchronous motor, d-c generator, power angle equipment in left foreground, and oscillograph in left background

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E. B. Kurtz is professor of electrical engineering with the University of Iowa, Iowa City, Iowa.

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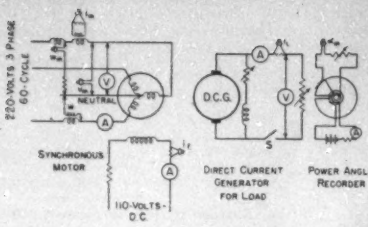

Figure 2 (left). Circuit diagram showing electrical connections and location of meters and oscillograph galvanometers

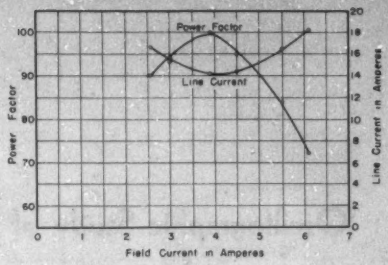

Figure 4. Synchronous motor "V" curves for 150 per cent load

taneous measure of the angle. Connections to an oscillograph makes possible a linear record of the angle. A direct indication for steady values of power angle can be obtained by means of a low-reading voltmeter.

The oscillograph galvanometers were arranged as shown in Figure 2 to record:

Motor voltage (phase)–V_m
Motor current (phase)–i_m
Motor power (phase)–w_m
Motor field current–i_f
Load current (d-c)–i_L
Power angle–α_m

Steady-State Values

A set of seven oscillograms for the application of load, and six for the removal of load were taken with the motor field current as the independent variable. The values of the steady-state motor phase current, phase power, phase power factor, and the load current of the generator, corresponding to the values of motor field currents used are tabulated in Table I. In Figure 4 these steady-state values of motor phase current and power factor are plotted against corresponding values of field current, giving the well known synchronous motor V-curves. The magnitude of load on all oscillograms was kept constant at approximately 150 per cent of rated load. The field current corresponding to minimum line current and maximum power factor for this load is seen to be 3.9 amperes.

Application of Load

Oscillograms

The sudden application of load is more critical than the sudden removal of load because the motor is more liable to fall out of step when load is suddenly applied. Whether the motor will fall out of step depends upon the magnitude of the load applied, the moment of inertia of the rotating parts, the effectiveness of the damper windings, and the value of field current. After the motor has been built the only remaining variable for a fixed load is the field current. In this study 150 per cent full load was suddenly applied with values of field current ranging from 2.57 to 6.10 amperes. Figures 5 to 11, inclusive, show the oscillograms for the application of load arranged in order from the lowest to highest values of field current. Each oscillogram shows the exact variation of the synchronous motor's phase current, phase power, power angle, and field current, and the d-c generator's load current. The records are labeled to correspond to the galvanometer-element notation shown in Figure 2.

Table I

Motor Field Current, Amperes	Phase Line Current, Amperes	Phase Power, Watts	Phase Power Factor	Generator Load Current, Amperes
2.57	16.6	1,960	0.90 lag	20.0
3.00	15.6	1,840	0.93 lag	21.0
3.50	14.0	1,820	0.986 lag	21.0
3.90	14.2	1,820	1.00	20.4
4.50	14.4	1,810	0.951 lead	20.3
5.50	16.5	1,780	0.84 lead	20.4
6.10	18.2	1,760	0.72 lead	20.0

Having records of instantaneous phase voltage, phase current, and phase power, made it possible to obtain the record of phase power factor by calculation. This is the reason all electrical quantities in the motor were recorded as phase quantities. Power factor could also be obtained from the watt-galvanometer trace by taking the ratio of the average power to the maximum amplitude.

Power Angle Variations*

The oscillograms strikingly show how the rotor falls back when load is suddenly applied, how it forges ahead due to having fallen back too far, and how it then drops back again, only to surge forward once more. This oscillation about the new phase position naturally continues until it is damped out.

Table II gives the values of the power angle, taken from the seven oscillograms.

*The small high-frequency 20-cycle superimposed ripple is due either to a slight mechanical vibration or to hunting by the auxiliary motor.

Figure 3 (left). View of power angle indicator showing the sliding brush in contact with high resistance wire

Figure 5. Oscillogram of sudden application of 150 per cent load with lowest value of field current of 2.57 amperes

Figure 6 (left). Oscillogram of sudden application of 150 per cent load with field current of 3.00 amperes

Figure 8. Oscillogram of sudden application of 150 per cent load with field current of 3.90 amperes

Table II. Power-Angle Variations During Application of Load

Time, seconds	Power Angle (electrical degrees)						
	Field Current						
	2.57	3.00	3.50	3.90	4.50	5.50	6.10
0.000	7.58	7.15	7.07	8.15	7.29	7.58	12.4
0.033	15.7	13.6	13.2	15.2	15.2	11.6	21.3
0.067	27.2	23.9	27.3	28.4	27.2	23.2	28.5
0.100	42.9	35.8	35.4	40.3	35.3	32.9	30.5
0.133	53.0	49.0	46.5	46.6	38.2	37.2	27.8
0.167	59.4	56.8	48.3	47.2	35.5	33.0	19.9
0.200	62.2	58.0	46.3	42.9	28.6	34.7	14.3
0.233	64.0	58.0	42.5	36.8	22.4	18.6	15.6
0.267	63.7	55.8	38.5	30.3	18.3	16.4	19.9
0.300	63.0	54.8	32.8	25.0	17.6	18.2	25.1
0.333	61.5	50.3	28.2	23.2	21.2	24.2	28.0
0.367	59.6	46.3	25.5	24.3	25.0	28.2	25.3
0.400	56.5	42.9	25.1	27.3	28.6	29.8	20.3
0.433	53.9	38.8	26.5	31.2	29.2	27.9	16.9
0.467	50.7	35.8	29.7	33.4	27.4	23.0	17.2
0.500	47.2	33.2	32.3	34.3	25.0	20.0	19.5
0.533	43.6	31.9	33.0	33.0	22.3	19.0	32.4
0.567	41.2	32.9	33.3	30.7	21.7	21.0	25.6
0.600	38.4	33.6	33.3	29.0	22.6	23.4	23.6
0.633	36.9	34.3	31.4	27.6	24.0	26.3	21.3
0.667	35.5	35.9	30.8	27.2	25.7	26.6	18.5
0.700	34.3	37.4	29.7	28.6	26.9	24.9	18.2
0.733	34.7	37.4	29.2	29.9	26.4	18.2	19.5
0.767	35.2	37.4	29.4	30.3	25.7	21.2	21.0
0.800	36.0	37.4	30.4	31.3	23.9	21.2	23.0
0.833	37.3	36.8	30.5	30.8	23.3	21.8	23.0
0.867	38.6	35.9	31.1	30.0	23.0	23.8	20.7
0.900	39.0	35.8	31.7	29.8	23.6	25.4	19.2
0.933	38.9	35.3	31.1	29.2	24.3	25.4	19.0
0.967	39.3	35.3	31.1	28.6	25.4	24.3	20.3
1.000	39.3	35.5	31.1	29.0	25.7	22.9	21.3
1.033				29.4	25.4	21.4	22.0
1.067				30.0	25.2	22.0	22.4
1.100				29.8	24.8	23.3	21.3
1.133				30.0	24.0	23.7	19.9
1.167					24.3	24.3	19.6
1.200					24.8	23.9	19.9
1.233					24.3	23.0	20.6
1.267					24.9	22.9	22.0
1.300					25.0	22.3	21.4
1.333					24.8	22.4	20.7
1.367						23.3	20.2
1.400						23.7	19.9
1.433						23.7	19.9
1.467						23.6	20.9
1.500						23.3	21.3

for each thirty-three thousandth of a second during the first one and a half seconds after the load was suddenly applied. Figure 12 shows these values of the power angle plotted against time for five of the values of field current used. (Only five curves are plotted to avoid confusion on the graph.)

It is noted from Figure 12 that the amplitude of the first oscillation decreases as the field current is increased. This is still more evident in Figure 13 where this magnitude in degrees is plotted against field current. The field current produces the force which holds the synchronous motor in step. As the field current is increased the rotor becomes more tightly coupled, and hence is not as free to oscillate as widely.

Hunting Frequency

It is further noted from Figure 12 that the frequency of oscillation increases as the field current is increased. This is the inverse of the amplitude of oscillation. In Figure 14 the hunting frequency is plotted against field current. The frequency is seen to vary from one cycle per second to slightly over four cycles per second. The hunting frequency would appear to decrease to zero at about 2.1 amperes field current, with a corresponding increase in the magnitude of the first oscillation. The motor, of course, would be likely not to stay in step, for an oscillation of 80 degrees would undoubtedly exceed the limit of stability. Actually, the lowest value of field current with which the motor remained in step was

Figure 7 (left). Oscillogram of sudden application of 150 per cent load with field current of 3.50 amperes

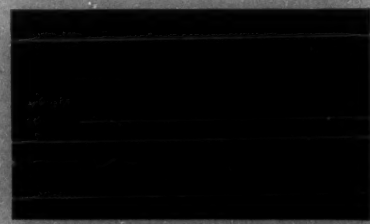

Figure 9. Oscillogram of sudden application of 150 per cent load with field current of 4.50 amperes

Figure 10 (left). Oscillogram of sudden application of 150 per cent load with field current of 5.50 amperes

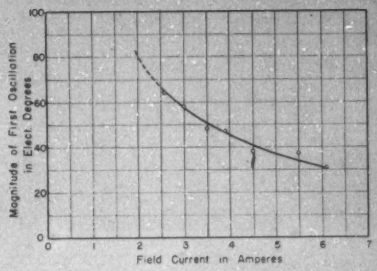

Figure 13. Magnitude of first oscillation during load application plotted against field current

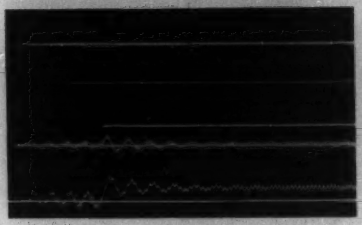

Figure 11 (left). Oscillogram of sudden application of 150 per cent load with highest value of field current of 6.10 amperes

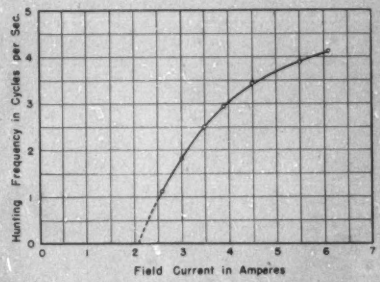

Figure 14. Hunting frequency during load application plotted against field current

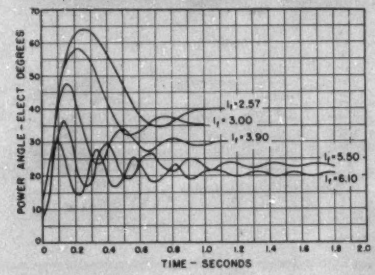

Figure 12 (left). Power angle variation during application of load plotted against time

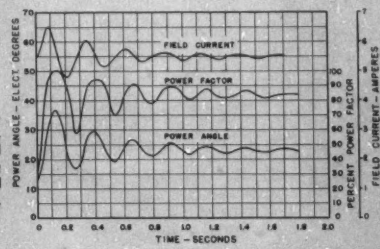

Figure 15. Variations in power angle, power factor and field current during application of load plotted against time. Field current = 5.50 amperes

found to be about 2.50 amperes. This corresponds to an oscillation of approximately 65 degrees. It can thus be said that under-excitation produces a slow but wide-swinging rotor, while over-excitation produces a fast but narrow-swinging rotor.

Other Variations

Further examination of the oscillograms shows the current and power variations to be proportional to the power angle for all values of field current. This follows, since the power angle is the angle between impressed and excitation voltages. An increase in the angle increases the resultant voltage, which in turn increases the current. Except for variations in power factor, the power varies directly with the current.

Analysis of the oscillograms shows the power factor to follow the power angle variations. In Figure 15, calculated values of power factor are plotted along side the power angle variation for the case of 5.50 amperes field current. The two quantities are seen to be closely related.

The variations in the field current are related to the power angle variations, since they are caused by the relative motion of rotor and revolving flux fields. A change in power angle means a change in rotor position. When this change occurs, a voltage is induced in the field winding. The high inductance in this winding causes the current change to lag the induced voltage.

Removal of Load

Motor Versus Generator Action

When the load is suddenly removed, the motor draws more power from the line than is momentarily required, and will therefore be accelerated towards its no-

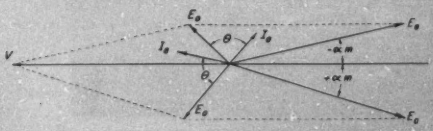

Figure 16. Vector diagram showing range of power angle swing

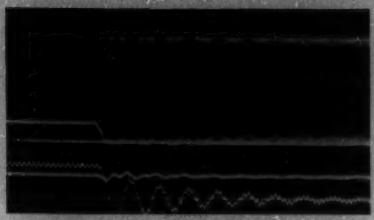

Figure 17 (left). Oscillogram of sudden removal of 150 per cent load with lowest value of field current of 2.57 amperes

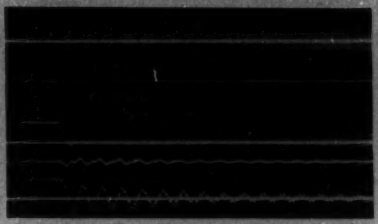

Figure 20. Oscillogram of sudden removal of 150 per cent load with field current equal to 4.50 amperes

Figure 18 (left). Oscillogram of sudden removal of 150 per cent load with field current equal to 3.00 amperes

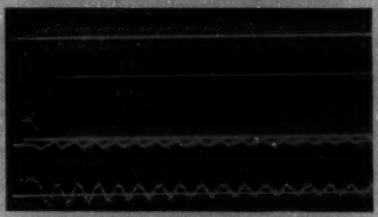

Figure 21. Oscillogram of sudden removal of 150 per cent load with field current equal to 5.50 amperes

Figure 19 (left). Oscillogram of sudden removal of 150 per cent load with field current equal to 3.90 amperes

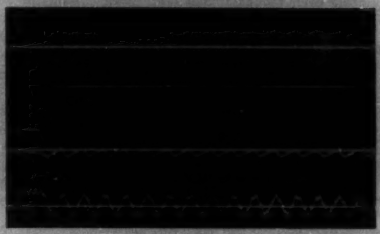

Figure 22. Oscillogram of sudden removal of load with largest value of field current of 6.10 amperes

load position. It cannot immediately take up the new angular position because the inertia of the rotor causes it to oscillate about the new phase position. If the entire load is removed and if the no-load power angle is small the oscillations will be likely to pass through the zero-angle position, in which case the motor will supply power to the line for short periods of time. The current will first decrease to a minimum at the zero-angle position and will then increase on the other side. This well-known phenomenon can be explained from the vector diagram shown in Figure 16, where

V = the applied voltage,
E_a = the excitation voltage,
E_e = the resultant voltage,
I_a = the current caused by E_e,
α_m = the power angle, and
$\theta = \text{Tan}^{-1} X_s/r_e$

As the motor hunts, the voltage E_a oscillates about the horizontal position thereby causing the resultant voltage E_e to vary in magnitude and phase position. When α_m is zero, E_e will be the algebraic difference between V and E_a. When α_m increases in either direction from the zero-angle position, E_e increases, and hence I_a increases. Therefore I_a has its minimum value at the zero power-angle position. When α_m is positive, the current is more than 90 degrees out of phase with E_a and hence represents motor action. The power thus changes from motor to generator power or vice versa when α_m passes through zero.

Oscillograms

Figures 17 to 22 inclusive are the oscillograms for the sudden removal of load arranged from the lowest to the highest values of field current. These oscillograms show a sudden decrease in current and power immediately after the load is dropped. At this time the rotor surges forward, due to excess power, to the zero power-angle position. The rotor, however, does not stop there, but on account of rotor inertia moves beyond the zero power angle position causing the machine to enter the generator zone on the vector diagram of Figure 16 (region of $-\alpha$). These transitions from motor to generator action or vice versa always occur at the points of zero power-angle. In fact, the zero-angle position can always be located on the oscillograms by erecting a perpendicular at the point corresponding to minimum line current, or of zero average power. By laying a straight edge across the oscillogram, it will be noted that perfect correspondence exists between these three zero values.

Hunting Frequency

The frequency of hunting during removal of load is plotted against field current in Figure 23. From the figure it can be seen that the frequency of hunting does not vary as widely during the removal of load as during application of load. The range covered is only 3 cycles per second to approximately $4^1/_2$ cycles per second. During application of load the range was

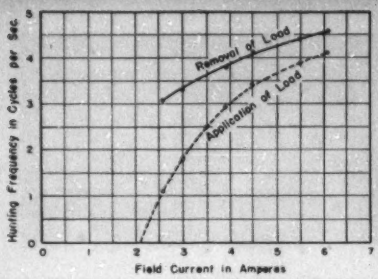

Figure 23 (left). Hunting frequency during removal of load plotted against field current

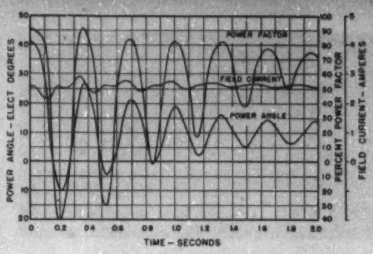

Figure 24. Power angle, power factor and field current variations during removal of load plotted against time (Field current = 2.57 amperes)

from 1 to 4 cycles per second. When the load has been removed the hunting represents more nearly the natural frequency of the rotating mass.

The damping action is also much less evident during removal of load. In Figure 22 for example, with 6.10 amperes field current the damping action is barely noticeable.

Similarly in Figure 11 prior to application of load, the motor was initially hunting even though it had been given several minutes to settle down.

Field Current Variation

During removal of load the field current becomes quite irregular as the rotor hunts through the zero angle position. At this point the direction of the induced electromotive force in the field circuit reverses. The current variation, of course, lags behind the voltage variation due to the inductance of the field circuit. The points of zero angle on the oscillograms therefore do not correspond to the points of maximum change in the field current.

During the application of load the power-angle oscillations were at all times on one side of the zero angle position and the induced voltage in the field circuit depended in magnitude and direction only on the instantaneous velocity of the field structure relative to the revolving field. The induced voltage changed direction only when the direction of motion changed. In the case of the removal of load, however, the induced voltage changes direction when the relative motion changes direction as well as when the rotor oscillates through the zero-angle position. The effect of the latter can be plainly seen on the oscillograms whenever the power angle crosses the zero angle line. An enlarged trace of these variations is shown in Figure 24 for removal of load with a field current of 2.57 amperes.

The power factor is seen to vary from 0 to 90 per cent as the motor hunts. It will also be noted that the power factor reverses polarity when the power angle passes through zero.

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No Discussion

Experimental Determination of the Capacitance of Heavy-Current Busses Comprised of Solid or Tubular Rectangular Conductors

ROBERT D. TEASDALE
ASSOCIATE AIEE

THOMAS JAMES HIGGINS
MEMBER AIEE

IN an investigation of circuit transient recovery voltage Adams, Skeats, Van Sickle, and Sillers[1] outline a procedure for determining sizes of the relaying equipment necessary for the protection of central station power systems (or other heavy current systems) under fault conditions. The first step in this procedure is to obtain the equivalent circuit of the system to be relayed. In turn, this step requires determination of the equivalent circuits of both the electrical apparatus and the busses comprising the system.

In general, no difficulty attends obtaining the equivalent circuits of the electric apparatus. Equations and data sufficient for calculations are to be found in textbooks, handbooks, and manufacturer's catalogs. Again, no difficulty attends obtaining the resistive and inductive impedances of the equivalent circuits of the busses. Long established and well verified equations and curves are available in other literature.[2]

Calculation of the capacitive impedances is quite a different matter. The present literature does not encompass equations from which can be calculated the capacitances (and thus the capacitive impedances) of one of the major single and 3-phase bus designs, which is rectangular strap conductors isolated in concrete cells. The basic configuration of such an arrangement, Figure 1, a rectangular conductor at line potential isolated in a rectangular concrete cell (of sides c and d) at zero reference potential. In fact, of most of the heavy busses used in practice, equations for the capacitance are not to be found, for the mathematical analysis required to effect these equations has proved intractable.

As stressed in the previously mentioned paper[1] it is most important that the designer be able to calculate accurately the required capacitances.

"Calculation of the transient recovery characteristics involves the determination of the natural frequencies...from the inductance and capacitances of the circuit";... "the capacitive coupling of adjacent circuits...must be taken into account"; "[it] is extremely important that the capacitances of the higher frequency circuits be estimated accurately".

The method commonly employed to obtain the required capacitances is that utilized by the authors of the above paper[1] in the use of a free-hand graphical plot of the electrostatic field of the bus and approximate calculation of the capacitance therefrom.[3] However, other than purely nominal values can thus be obtained only by one skilled in such plotting, and even a skilled plotter requires appreciable time if any considerable accuracy is to be obtained, especially if a number of plottings is necessitated by an extensive system.

The available alternative methods for obtaining the field accurately have obvious disadvantages; numerically, by use of one of several finite difference procedures;[4] graphically, by use of exact drafting procedures;[4,5] or experimentally, by use of an electrolytic tank[6,7] or conducting plate.[7,8] The first requires very heavy computation; the second lengthy and tedious drafting; the third special apparatus. They are time consuming and are indirect methods (for the capacitance must yet be calculated from the field plot). In all, the accuracy with which the capacitance is obtained hinges on the fineness of the field plot and the subsequent interpretation and averaging of the

Table I. Data of Run 1 (r_1 = 6.99 Centimeters)

Inner Electrodes Number	r_1, Centimeter	ln r_1	r_2/r_1	ln r_2/r_1	R_s, Ohm-centimeter	S_s, 10^{12} Darafs	C_s, 10^{-12} Farads
2	6.03	1.80	1.16	0.149	3,210	0.279	3.58
3	5.28	1.66	1.33	0.282	6,350	0.544	1.84
4	4.17	1.43	1.67	0.515	11,000	0.957	1.05
5	3.55	1.27	1.97	0.676	14,900	1.30	0.772
6	3.17	1.15	2.20	0.790	16,900	1.47	0.680
7	2.80	1.03	2.50	0.915	19,500	1.70	0.590
8	2.15	0.767	3.25	1.18	24,800	2.16	0.463
9	1.76	0.565	3.97	1.38	28,700	2.50	0.401
10	1.20	0.182	5.83	1.76	36,500	3.18	0.315

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ROBERT D. TEASDALE is a graduate student and Gerard Swope Fellow in the department of electrical engineering with the Illinois Institute of Technology, Chicago, Ill., and THOMAS JAMES HIGGINS is a professor of electrical engineering, with the University of Wisconsin, Madison, Wis.

The authors are indebted to Doctor George Polya for providing us with equation 11 and to Donald Simmons for directing our attention to references 5, 6, and 8.

This paper is based upon a thesis supervised by Thomas James Higgins and submitted by Robert D. Teasdale to the faculty of the Illinois Institute of Technology in June, 1947, in partial fulfillment of the requirements for the degree of master of science in electrical engineering.

curvilinear mesh marked out by the mutually orthogonal flux and equipotential lines. Accordingly, regardless of the method chosen accurate calculation of the capacitance is an involved and lengthy process.

In consequence of these disadvantages, it is evident that it is most desirable to have a simple direct method which both quickly yields the capacitance of a bus of arbitrary geometry and lends itself to the rapid preparation of curves or equations for the capacitance of those configurations commonly encountered in practice. Such a method is available, but it would appear that this method is not widely known and has had very limited use. It is not mentioned in textbooks or handbooks. An exhaustive search of the literature has revealed only one published application, namely, for determination of the capacitances of a multiphase cable.[6,8]

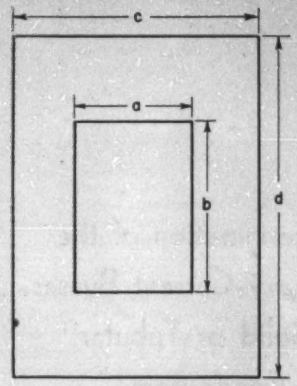


Figure 1. Cross section of coaxial rectangular capacitor

Table II. Dimensions of Electrodes Used in Runs 2, 3, 4, 5, and 6

Electrode Number	Nominal Dimensions, In.	Outer Dimensions, Cm.	Inner Dimensions, Cm.
1	0.5×0.5	1.28×1.28	.....
2	0.5×1	1.45×2.72	.....
19	0.5×2	1.28×5.09	.....
20	0.5×3	1.28×7.65	.....
21	0.5×4	1.28×10.20	.....
3	1×1	2.70×2.74	2.51×2.54
4	1×2	2.73×5.27	2.54×5.08
5	1×3	2.63×7.78	2.45×7.60
6	1×4	2.70×10.35	2.52×10.14
7	1×5	2.68×12.89	.....
8	2×2	5.23×5.26	5.06×5.09
9	2×3	5.28×7.80	5.10×7.61
10	2×4	5.28×10.32	5.10×10.13
11	2×5	5.20×12.79	5.03×12.61
12	3×3	7.78×7.85	7.60×7.64
13	3×4	7.80×10.28	7.62×10.10
14	3×5	7.78×12.86	7.60×12.70
15	4×4	10.32×10.33	10.20×10.17
16	4×5	10.40×12.77	10.22×12.59
22	4×8	.....	10.13×20.30
17	5×5	12.82×12.88	12.68×12.72
18	6×6	15.40×15.35	15.20×15.15

Fundamental Theory

If the plates of a cylindrical capacitor of capacitance C and permittivity ϵ are considered as electrodes with the space between them filled with a conducting medium of resistivity ρ, it follows from well-known theory[9] that in rationalized units the resistance R between the electrodes is related to the corresponding capacitance through the equation

$$C = \epsilon\rho/R = \epsilon_o\epsilon_r\rho/R \qquad (1)$$

wherein the permittivity ϵ is the product of the absolute permittivity ϵ_o and the relative permittivity ϵ_r (≡dielectric constant K).

This equation suggests that the capacitance of a cylindrical capacitor can be determined by constructing metal electrodes of the same shapes as the plates of the capacitor, filling the space between these electrodes with a suitable liquid conducting medium of known resistivity ρ, measuring the resistance R with a Wheatstone bridge, and calculating C from equation 1.

Though this equation has long appeared in books on electromagnetic theory, and though, as is confirmed by the experimental work discussed below, the method is most feasible, it seems that the experimental method is not well known. At least, as remarked before, it has been used very little. Curiously, it happens that most investigators desiring knowledge of the capacitance of a certain configuration have utilized the same experimental setup for mapping the electric field and have then determined from the graphical field map—hence indirectly and with loss of accuracy—the desired capacitance. This procedure is typified in the work of Sacchetto.[10]

Experimental Apparatus

Figure 2 illustrates the apparatus utilized for the experimental work. A glass evaporating dish ten inches in diameter and four inches high contains the conducting medium. After testing several fluids, distilled water was selected as most satisfactory for general use. It is both inexpensive and easily procurable, and it affords a range of resistance which enables accurate measurement on an ordinary 4-decade Wheatstone bridge.

The circular brass electrodes, four inches high, are cut from standard sizes of brass tubing. The rectangular copper electrodes, 2.25 inches high, are made from copper strips, cut from standard 1/16-inch copper sheeting, bent to size in a mechanical bending machine, and brazed together at the joint. The internal and external dimensions of the circular electrodes are tabulated in Table I; those of the rectangular electrodes are tabulated in Table II.

The terminals of the bridge are connected to the electrodes by leads terminated in alligator clips. The total series resistance of leads, clips, and a typical pair of electrodes is less than 0.1 ohm, a quantity which is negligible by

Figure 2. Experimental apparatus

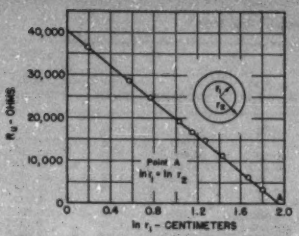


Figure 3. Resistance between circular electrodes for one centimeter depth of distilled water

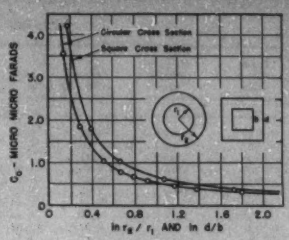


Figure 4. Capacitance per centimeter length of capacitor

comparison with the several hundred or several thousand ohms of the conducting medium between the electrodes.

In use, the height of the conducting medium is measured with a steel scale. Coaxialness of the electrodes is assured by placing beneath the glass container a sheet of paper on which the properly located cross sections of the electrodes (circular or rectangular, as used) are drawn to size.

The electrodes, polished with fine emery cloth, are prepared for use by immersing them in a dilute solution of hydrochloric acid for ten minutes; then removing the drops of solution clinging to their surfaces by thorough scrubbing with a stiff brush under running water; and finally soaking them in distilled water for 24 hours, during which time the small traces of acid yet remaining in fine cracks or minute pits in the surface of the electrodes pass into the distilled water. During cleaning, and during the experimental work, the electrodes are handled with tongs to avoid possible contamination of the surfaces by dust, body acids, or other substances commonly present on the worker's hands.

A considerable amount of experimentation was required to obtain a conducting medium of constant resistivity. Preliminary runs with the circular electrodes revealed that the resistivity of the distilled water, if used as obtained directly from a chemical store room, changed rather rapidly with time. For example, two runs made three hours apart on the same setup, revealed that the resistivity had changed 18 per cent during this period. Initially, this change of resistivity with time was attributed to one or more of the following causes:

1. Contamination of the water by particles of dust settling in it from the atmosphere or by absorption of gases from the atmosphere.

2. Contamination of the water by particles of dust which settled on the electrodes while they were not in use.

3. Electrolysis or polarization produced by the d-c bridge current passing through the possibly slightly impure distilled water.

Systematic exploration of these possibilities resulted in finding

1. That the distilled water should be kept in a large glass beaker, but with the open top covered with paper, so that the water is protected from the dust, yet essentially is open to the atmosphere insofar as absorption of gases is concerned. This is allowed to stand for approximately 36 hours so that it comes into equilibrium insofar as appreciable change in resistivity over the considerable time required to take a large set of experimental readings is concerned.

2. That to avoid contamination of the water by dust collected on the electrodes and to avoid disturbing the stableness of the water, it is desirable to keep those electrodes not in use in the water just mentioned (or in glass beakers of water prepared at the same time and in the same fashion).

3. That essentially identical readings are obtained with either d-c or a-c Wheatstone bridges. As d-c bridges are somewhat more advantageous for general work, all data was obtained with a commercial 4-decade d-c bridge.

Table III. Data of Run 2

Inner Electrode, Number	Nominal Dimensions $a \times b$, In.	d/a (d = c = 15.17 cm.)	d/b (d = c = 15.17 cm.)	ln(d/b) (d/c = 1)	R_u, Ohms	S_o, 10^{12} darafs	C_o, 10^{-12} farads
1	0.5×0.5	11.9	11.9	2.48	41,700	3.63	0.276
2	0.5×1	10.5	5.58	1.72	35,800	3.12	0.321
19	0.5×2	11.8	2.98	1.09	29,300	2.56	0.390
20	0.5×3	11.8	1.98	0.68	23,500	2.05	0.488
21	0.5×4	11.8	1.48	0.400	18,600	1.62	0.617
2	1×0.5	5.58	10.5	2.35	35,800	3.12	0.321
3	1×1	5.62	5.62	1.73	30,600	2.66	0.376
4	1×2	5.55	2.88	1.06	23,850	2.08	0.480
5	1×3	5.76	1.95	0.667	18,750	1.63	0.613
6	1×4	5.62	1.47	0.382	14,600	1.28	0.787
7	1×5	5.65	1.18	0.165	9,020	0.795	1.26
19	2×0.5	2.98	11.8	2.48	29,300	2.56	0.390
4	2×1	2.88	5.55	1.73	23,850	2.08	0.480
8	2×2	2.90	2.90	1.07	18,900	1.64	0.608
9	2×3	2.87	1.94	0.662	14,800	1.29	0.778
10	2×4	2.87	1.47	0.384	11,080	0.962	1.04
11	2×5	2.92	1.19	0.171	7,170	0.624	1.60
20	3×0.5	1.98	11.8	2.47	23,500	2.05	0.488
5	3×1	1.95	5.76	1.75	18,720	1.63	0.614
9	3×2	1.94	2.87	1.05	14,750	1.28	0.779
12	3×3	1.94	1.94	0.662	11,460	0.996	1.03
13	3×4	1.94	1.48	0.388	8,400	0.730	1.37
14	3×5	1.95	1.18	0.166	5,280	0.460	2.18
21	4×0.5	1.49	11.8	2.47	18,600	1.62	0.617
6	4×1	1.47	5.62	1.73	14,600	1.27	0.788
10	4×2	1.47	2.87	1.05	11,070	0.961	1.04
13	4×3	1.48	1.95	0.665	8,400	0.730	1.37
15	4×4	1.47	1.47	0.381	6,395	0.556	1.80
16	4×5	1.46	1.19	0.174	4,110	0.358	2.80
7	5×1	1.18	5.65	1.73	9,020	0.785	1.27
11	5×2	1.19	2.92	1.07	7,170	0.624	1.61
14	5×3	1.13	1.94	0.661	5,290	0.460	2.18
16	5×4	1.19	1.46	0.377	4,110	0.358	2.80
17	5×5	1.18	1.18	0.166	2,680	0.233	4.29

Experimental Data

Five runs were made. Run 1, on circular electrodes, enables calculation of the resistivity of the conducting medium, provides experimental confirmation of the general method, and furnishes a check on the correct functioning of the apparatus. The data, taken with a fixed outer electrode and inner electrodes of various radii, are tabulated in Table I.

Runs 2, 3, and 4, on rectangular electrodes enables derivation of a general equation for the capacitance of a coaxial cylindrical capacitor of square cross section and yield data for families of curves furnishing the capacitance of a wide range of cylindrical capacitors of general rectangular cross section. The data taken with fixed outer electrodes (ratio of ex-

ternal sides $d/c=1$, 1.23 and 1.67) and inner electrodes of various sizes are tabulated in Tables III, IV, and V.

Run 5, on square electrodes, provides experimental confirmation of a point of general theory effected by dimensional analysis, which is of general importance in calculating capacitance. The data are tabulated in Table VI.

Calculation of Resistivity

From well known theory[8] the resistance R of a length L of the conducting medium between two indefinitely long, coaxial circular cylindrical electrodes is

$$R=(\rho/2\pi L)\ \ln\ (r_2/r_1)\ \text{ohms} \qquad (2)$$

wherein r_1 is the radius of the inner electrode, r_2 the radius of the outer electrode, and ρ the resistivity of the conducting medium between the electrodes Accordingly, if $R_u=RL$ (where R_u is numerically equal to the resistance of a unit length of the cylindrical configuration).

$$R_u=(\rho/2\pi)\ \ln\ (r_2/r_1)\ \text{ohm-centimeters} \qquad (3)$$

Inasmuch as r_2/r_1 is a dimensionless ratio, r_1 and r_2 can be measured in any convenient unit of length. If, as is usually desirable, R is to be measured in ohms and R_u in ohm-centimeters, ρ must be measured in ohm-centimeters.

Equation 3 can be written in the form

$$R_u=-(\rho/2\pi)\ \ln\ r_1+(\rho/2\pi)\ \ln\ r_2\ \text{ohm-centimeters} \qquad (4)$$

In equation 4, let r_2 be of fixed value and the equation be interpreted as expressing R_u as a function of $\ln\ r_1$. Then equation 4 is that of a straight line, whereof $(-\rho/2\pi)$ is the slope m and $[(\rho/2\pi)\ln\ r_2]$ is the ordinate intercept B. Accordingly, if the resistances between a fixed outer electrode of radius r_2 and various inner electrodes of radius r_1 be measured, and if from this data R_u is plotted as a function of $\ln\ r_1$, it is to be expected that the resulting curve will be a straight line.

The curve of Figure 3, plotted from the data of run 1, made in the manner stated, is precisely such a straight line. Accordingly, as in run 1, $r_2=6.99$ centimeters, the equation of this straight line is

$$R_u=-0.159\rho\ \ln\ r_1+0.310\rho\ \text{ohm-centimeters} \qquad (5)$$

The slope m of this straight line is -0.159ρ; the ordinate intercept B is 0.310ρ. In consequence, $\rho=m/(-0.159)=B/0.310$ ohm-centimeters. From Figure 2 the value of B is 40,600. Therefore, $\rho=40{,}600/0.310=1.31\times10^5$ ohm-centimeters. Corroboratively, measurement of ρ with a standard conductivity cell yields essentially the same value.

These several agreements between theory and experiment indicate that the apparatus is functioning correctly and provide, as is discussed in the next section, experimental confirmation of the general procedure outlined under Fundamental Theory.

Table IV. Data of Run 3

Inner Electrode, Number	Nominal Dimensions a×b, In.	d/a d=12.6 cm.	d/b c=10.2 cm.	ln(d/b) d/c=1.23	R_u, Ohms	S_o, 10^{12} Darafs	C_o, 10^{-12} Farads
1	0.5×0.5	9.83	9.83	2.29	36,300	3.16	0.316
2	0.5×1	8.70	4.62	1.53	30,000	2.61	0.338
19	0.5×2	9.83	2.47	0.905	23,000	2.00	0.500
20	0.5×3	9.83	1.64	0.495	175,000	1.52	0.658
21	0.5×4	9.83	1.23	0.207	11,600	1.01	0.990
2	1×0.5	4.65	8.67	2.16	29,760	2.59	0.386
3	1×1	4.67	4.59	1.52	25,170	2.19	0.457
4	1×2	4.62	2.39	0.869	18,100	1.57	0.635
5	1×3	4.90	1.62	0.482	13,500	1.18	0.851
6	1×4	4.67	1.21	0.194	8,910	0.780	1.29
19	2×0.5	2.48	9.81	2.28	23,300	1.95	0.512
4	2×1	2.39	4.60	1.53	17,270	1.50	0.667
8	2×2	2.41	2.38	0.868	12,600	1.10	0.911
9	2×3	2.39	1.62	0.482	9,200	0.800	1.25
10	2×4	2.39	1.22	0.197	5,850	0.510	1.97
20	3×0.5	1.15	9.81	2.28	14,100	1.23	0.814
5	3×1	1.62	4.78	1.57	10,800	0.940	1.06
9	3×2	1.61	2.38	0.868	7,500	0.653	1.53
12	3×3	1.62	1.60	0.472	5,440	0.470	2.12
13	3×4	1.62	1.22	0.200	3,570	0.310	3.22

Square Cylindrical Capacitors

Substituting $\rho=1.31\times10^5$ ohm-centimeters, $\epsilon_o=(1/36\pi)10^{-11}$ farads/centimeter*, and $\epsilon_r=1$ in equation 1 yields

$$C_o=1.16\times10^4/R_u\ \text{micromicrofarads/centimeter} \qquad (6)$$

where C_o indicates the capacitance per centimeter length of the capacitor with *air* as dielectric. Correspondingly, the elastance $S_o=1/C_o$ is

$$S_o=8.67\times10^{-8}R_u\ \text{megamegadarafcentimeters} \qquad (7)$$

Values of the capacitances C_o and of the elastance S_o of circular cylindrical capacitors, calculated from the data of run 1, are tabulated in Table I. Values of the capacitance C_o and of the elastance S_o of square cylindrical capacitors, calculated from the data of run 2, are tabulated in Table VII. These two sets of data afford the curves of Figures 4 and 5.

In Figure 5, the straight line obtained for the circular cross section is that of the

Figure 5. Elastance of a centimeter length of capacitor

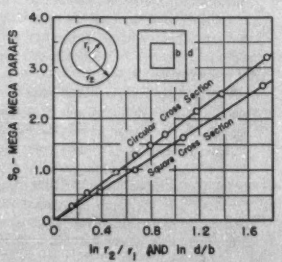


Table V. Data of Run 5

Inner Electrode, Number	Nominal Dimensions a×b, In.	d/a d=12.7 cm.	d/b c=7.60 cm.	ln d/b d/c=1.67	R_u, Ohms	S_o, 10^{12} Darafs	C_o, 10^{-12} Farads
1	0.5×0.5	9.91	9.94	2.30	30,300	2.64	0.380
2	0.5×1	8.75	4.68	1.54	24,300	2.11	0.474
19	0.5×2	9.91	2.50	0.915	18,750	1.63	0.613
20	0.5×3	9.91	1.66	0.506	14,100	1.23	0.816
21	0.5×4	9.91	1.34	0.218	9,200	0.800	1.25
2	1×0.5	4.66	8.75	2.17	23,500	2.04	0.489
3	1×1	4.70	4.65	1.54	19,200	1.67	0.599
4	1×2	4.65	2.41	0.880	13,800	1.20	0.834
5	1×3	4.82	1.63	0.488	10,350	0.900	1.11
6	1×4	4.70	1.23	0.203	7,200	0.626	1.60
19	2×0.5	2.49	9.92	2.30	14,800	1.29	0.777
4	2×1	2.41	4.65	1.54	11,100	0.966	1.04
8	2×2	2.42	2.41	0.880	7,690	0.669	1.50
9	2×3	2.41	1.63	0.488	5,550	0.483	2.07
10	2×4	2.41	1.23	0.203	3,960	0.345	2.90

Table VI. Data of Run 5 ($d/c = 1$)

Nominal Electrode, Dimensions, Inches	a=b Centimeters	c=d Centimeters	d/a=d/b	ln d/b	R_u, Ohms	S_u, 10^{12} Darafs	C_u, 10^{-12} Farads
2×2 and 6×6	5.25	15.17	2.88	1.06	18,650	1.63	0.615
1×1 and 3×3	2.72	7.62	2.80	1.03	18,500	1.61	0.620
3×3 and 6×6	7.81	15.17	1.94	0.663	11,700	1.02	0.985
2×2 and 4×4	5.25	10.19	1.94	0.663	11,700	1.02	0.985
1×1 and 2×2	2.72	5.08	1.87	0.623	11,600	1.01	0.990
4×4 and 6×6	10.32	15.17	1.47	0.384	6,450	0.560	1.79
2×2 and 3×3	5.25	7.62	1.45	0.372	6,390	0.555	1.80

known equation for the elastance of a circular cylindrical capacitor, $S_u = (1/2\pi\epsilon)\ln(r_2/r_1)$.

Obviously, this agreement between theory and experiment is to be expected: for the same experimental data affords the theoretically predicted curve of Figure 3. But it is a new and most interesting result that over the range of b/d utilized in the experimental work the curve for the square cylindrical capacitor is also a straight line.

Scaling the slope of this straight line yields $m = 1.532 \times 10^{12}$. Accordingly, its equation is

$$S_u = 1.532 \ln (d/b) \text{ megamegadaraf-centimeter} \tag{8}$$

If the slope is written in the form $m = k/2\pi\epsilon_0 = 1.532 \times 10^{12}$, the substitution of $\epsilon_0 = (1/36\pi)10^{-11}$ yields $k = 0.852$. Hence, the experimentally determined equation of the elastance of a square cylindrical capacitor, written in a form paralleling the theoretically derived equation for the elastance of a circular cylindrical capacitor, is

$$S_u = (1/2\pi\epsilon) \ln (d/b)^{0.852} \text{ daraf-centimeters} \tag{9}$$

*In meter-kilogram-second units as used in this paper, the absolute permittivity is $\epsilon_0 = (1/36\pi)10^{-9}$ farad per meter, whence the value of $(1/36\pi)10^{-11}$ farads per centimeter used above.

and the corresponding equation for the capacitance is

$$C_u = 2\pi\epsilon/\ln (d/b)^{0.852} \text{ farads/centimeter} \tag{10}$$

Equations 9 and 10 are, therefore, general equations from which the elastance and the capacitance of a square cylindrical capacitor of arbitrary dimensions can be calculated *for* (as is shown in the next section) all values of the ratio d/b which are likely to be encountered in practice.

Analytic Substantiation of Equation 10

Polya and Szego[11,12] have shown that the capacitance C_u of a cylindrical capacitor whereof the cross section is the annular area bounded by two similar, that is, the ratio of corresponding sides is a constant) concentric parallel-sided polygons of n sides lies between the limits

$$\sum_{i=1}^{n} \epsilon A_i/D_i \leq C_u \leq \frac{L'-L}{L \ln (L'/L)} \sum_{i=1}^{n} \epsilon A_i/D_i \tag{11}$$

whereof A_i is the length of a side of the inner polygon, D_i the perpendicular distance of this side from the corresponding side of the outer polygon, L is the length of the perimeter of the inner polygon, and L' is the length of the perimeter of the outer polygon. In the case of a square capacitor of inner side b and outer side d, $D_i = (d-b)/2$, $A_i = b$, $L = 4b$ and $L' = 4d$. Substituting in equation 11 yields

$$8\epsilon b/(d-b) \leq C_u \leq 8\epsilon/\ln(d/b) \tag{12}$$

Rewriting equation 10 in the form of the upper bound of equation 12 gives

$$C_u = 7.37\epsilon/\ln(d/b) \tag{13}$$

The values of the upper and lower bounds of equation 12 and the values of C_u from equation 13, as calculated from the data of Table VII (the data used in plotting the curve of Figure 5 from which equation 13 stems), are tabulated in Table VIII. Over the experimental range of d/b the values from equation 13 fall between the upper and lower bounds, hence are limited in accord with theory. It is to be noted that over this range of d/b (3 to 15) the theoretical lower bound is very poor. This condition is to be expected; for this lower bound is an obvious approximation stemming from the equation for the capacitance of a parallel plate capacitor, hence is a good approximation only for small values of $(d-b)/2$, thus for values of $d/b \cong 1$. Solving the equation $8b/(d-b) = 7.37/\ln(d/b)$ by trial yields $d/b = 1.15$. Accordingly, for all values of d/b greater than 1.15, hence certainly for all values to be encountered in practice, equation 13 yields values between the theoretical limits. Accordingly Polya and Szego's analysis provides striking mathematical evidence of the correctness of the experimentally derived equation.

In view of this confirmation, and in view of the excellent linearity of the experimental data, it can safely be said that equation 13 can be used to calculate the values of the capacitances of those square cylindrical capacitors likely to be encountered in practice. In substantiation of this statement are to be noted the capaci-

Figure 6. Elastance of a centimeter length of capacitor: $d/c = 1$

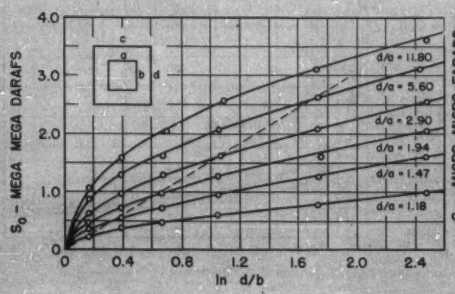
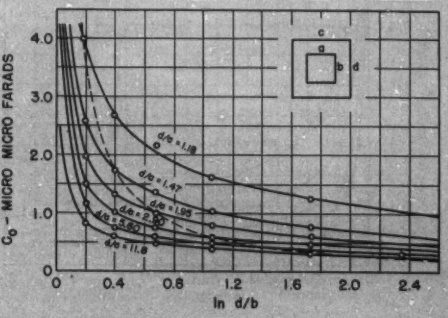


Figure 7. Capacitance per centimeter length of capacitor: $d/c = 1$



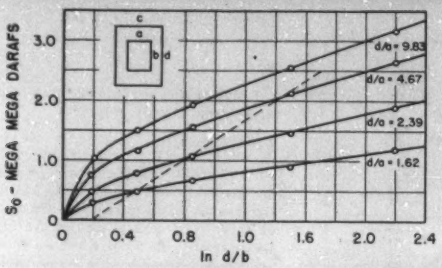


Figure 8. Elastance of a centimeter length of capacitor: $d/c = 1.23$

tances C_u of two square capacitors of ratio of sides $d/b=2$ and $d/b=3$. These values, computed in connection with another part of the thesis from which this paper stems, were calculated in well known fashion from flux plots effected with considerable accuracy by a finite difference procedure, the so-called relaxation process.[13] The two values are 0.925 and 0.536 micromicrofarad per centimeter; the corresponding values from equation 13 are 0.975 and 0.575 micromicrofarad per centimeter. The per cent differences in corresponding values are 4.1 and 6.8 per cent. In view of the difficulty associated with accurately calculating capacitance from a flux plot, the agreement of corresponding values is excellent; further, it is to be expected that the values calculated from equation 13 are the more correct values.

Rectangular Cylindrical Capacitors

The geometry of a rectangular cylindrical capacitor with cross section as in Figure 1 is completely determined by knowledge of one side of the cross section and the ratios of the other sides to it; for example, by d, d/a, d/b and d/c. Now as in meter-kilogram-second units C_u is of dimensions farad-meter^{-1} and ϵ is also of dimensions farad-meter^{-1}, it follows from the theory of dimensional analysis[14] that the capacitance C_u of a rectangular cylindrical capacitor can be written in the form

$$C_u = \epsilon F(d/a,\ d/b,\ d/c) \tag{14}$$

where F is an unknown dimensionless function of the ratios d/a, d/b, and d/c.

Equation 14 indicates that rectangular cylindrical capacitors can be of different dimensions a, b, c, and d, yet if the ratios d/a, d/b, and d/c are alike, the capacitors will be of identical capacitance. Corroboratively, for each of three different sets of ratios, the two or three corresponding values of C_o tabulated in Table VI (from data taken in run 5) are in excellent agreement.

The slight discrepancies are attributable to experimental error inherently imposed by the slight difference in corresponding like geometric ratios.

This excellent agreement between theory and experiment provides further, and striking confirmation of the reliability of the direct experimental methods of determining capacitance.

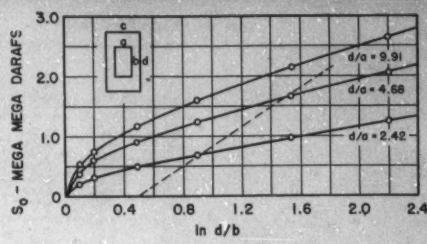


Figure 10. Elastance of a centimeter length of capacitor: $d/c = 1.67$

Cylindrical Capacitors of Arbitrary Dimensions

It follows from equation 14 that a 4-dimensional plot of capacitance as a function of the three ratios d/a, d/b, and d/c would yield the capacitance of a rectangular cylindrical capacitor of arbitrary dimensions. A systematic method of obtaining the required experimental data for such a plot is to hold, in turn, two of the ratios fixed and to vary the third over any desired range. Of course, the data so obtained cannot be obtained in a 4-dimensional space. But for engineering purposes it suffices to plot the data by preparing curve sheets whereon for a given ratio of d/c a parametric family of curves of C_o or S_o are plotted as functions of d/b, the parameter being d/a. Figures 6 to 11, prepared from the data of Tables 3, 4 and 5, comprise precisely such families of curves. The dashed lines, drawn through the data points for square capacitors, afford a check on the experimental values. For as in Figure 5, these points fall on a

Figure 9. Capacitance per centimeter length of capacitor: $d/c = 1.23$

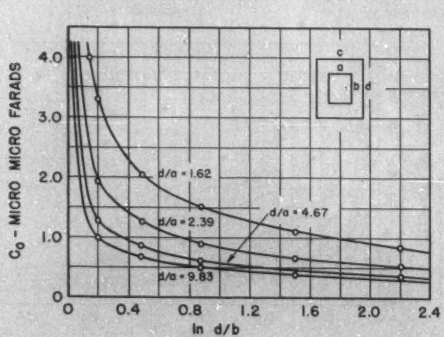


Figure 11. Capacitance per centimeter length of capacitor: $d/c = 1.67$

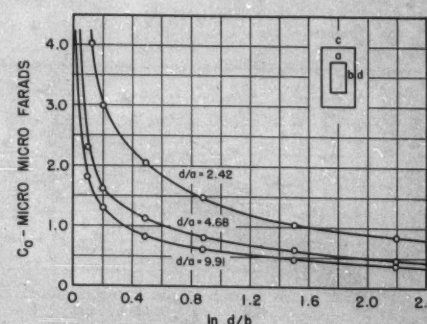


Table VI'. Data on Square Electrodes of Run 2

Inner Square Electrode, Number	Nominal Dimension $a \times b$, In.	b	d/b d=15.17 cm.	d−b	ln d/b d/c=1.0	S_0, 10^{12} Darafs	C_0, 10^{-12} Farads
3	1×1	2.72	5.57	12.45	1.72	2.66	0.376
8	2×2	5.35	2.89	9.92	1.07	1.64	0.608
12	3×3	7.80	1.94	7.37	0.662	0.996	1.03
15	4×4	10.3	1.47	4.87	0.384	0.556	1.80
17	5×5	12.82	1.18	2.35	0.165	0.233	4.29

Table VIII. Comparison of Experimental and Theoretical Values

Inner Electrode, Number	Cu^{-}, 10^{-12} Farads	Cu_t, 10^{-12} Farads	Cu_t^{+} 10^{-12} Farads
3	0.155	0.376	0.411
8	0.375	0.608	0.661
12	0.75	1.03	1.07
15	1.492	1.80	1.84
17	3.855	4.29	4.29

straight line in accordance with the theory discussed under Square Cylindrical Capacitors. In a similar fashion, a set of curves can be prepared for any shape of cylindrical capacitor and over as great a range of geometry as is desired. In particular, it would be a simple matter to prepare a set of curves for the capacitance of a coaxial line comprised of an outer conductor of rectangular cross section and inner conductor of circular cross section. This configuration, of importance in communication work, is intractable to exact analytic solution; and existing approximate equations for the capacitance, obtained by intricate mathematical analysis, are valid for only a limited range of geometry.[15,16]

Capacitors of Arbitrary Geometry

From well-known theory, the problem of determining the capacitance of a capacitor comprised of plates of arbitrary geometry, arbitrarily located relative to one another, and containing dielectric which, though homogeneous throughout a region, may vary in permittivity from one region of space to another, hinges on determining a potential function V that:

1. Satisfies Laplace's equation everywhere in space.
2. Vanishes at infinity, except possibly for a constant.
3. Takes on assigned constant values over each of the plates.
4. Satisfies the equations $V' = V''$ and $\epsilon'(\partial V'/\partial n) = \epsilon''(\partial V''/\partial n)$ at each point in the interface between two regions of permittivity ϵ and ϵ'', n being the outward normal to the interface.

The potential V being obtained, the surface integral of $\epsilon(-\partial V/\partial n)$ over one of the plates yields the charge on the capacitor; whence the ratio of this charge to the known potential difference yields the desired capacitance.

Correspondingly, the problem of determining the conductance between two electrodes of arbitrary geometry, arbitrarily located relative to one another, and immersed in a conducting medium which, though homogeneous throughout a region may vary in conductivity σ from one region of space to another, hinges on determining a potential function V satisfying conditions precisely the same as those shown before except for the replacement of ϵ by σ. The potential V being determined, the surface integral of $\sigma(-\partial V/\partial n)$ over one of the electrodes yields the current between the electrodes; whence the ratio of this current to the potential difference yields the desired conductance. Accordingly, the top problems are mathematically identical and knowledge of one yields the other through appropriate interchange of ϵ and σ. In consequence, it follows that the direct experimental method of determining capacitance is not confined to cylindrical capacitors, but can be utilized for any shape of capacitor by obvious extension of the procedure outlined previously.

Inductance of High Frequency Transmission Lines

It is well known[9] that the inductance L_u and the capacitance C_u of a high-frequency transmission line are related by the equation $L_u C_u = \mu\epsilon$. Substituting from equation 1 yields

$$L_u = (\mu/\rho) R_u \tag{15}$$

Accordingly, the inductance of such a line can be determined experimentally in the fashion discussed before. This method ought to be particularly useful for obtaining the inductances of configurations of split-conductor transmission lines such as four- or eight-wire exponential transmission lines.

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Discussion

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): I note with much interest the paper by Teasdale and Higgins in the determination of capacitance by the use of resistance measurements. In our work to which the authors refer, we found possible far greater precision than for other methods. Not only was good precision obtainable but the method is more simple and rapid in use than alternate physical methods that have been pursued.

There is little more to be said in discussion of a paper such as this than that the work appears to have been carried on efficiently and that the results have been presented in a manner to make them readily and widely available for the purpose intended.

An Optical Radar for Surveying

W. W. HANSEN
NONMEMBER AIEE

Synopsis: An instrument is described which uses radar techniques to make a survey of terrain. Distances are determined by measuring the transit time of pulses of light traveling between the instrument and a retrodirective reflector placed at the point whose relative position is to be found. Angles are measured by adjusting the position of the optical transmitter to obtain a maximum pip on a cathode-ray tube.

Introduction and General Description

THE instrument is capable of making a survey of terrain when conventional survey methods would be too slow or when long traverses over inaccessible terrain are needed. The choice of light instead of microwave radar was made because of the improved resolution and because reflections of microwave radar from other than the target would limit the usefulness of the equipment.

In the method used, distance or range is determined by measuring the transit time of pulses of light traveling to and from a retrodirective reflector placed at a point whose relative position is to be found. Angles are determined by adjusting the position of the optical transmitter to produce a maximum return "pip" on a cathode-ray tube. While the accuracy of the survey which can be obtained with this instrument is not as good as that which can be achieved by conventional surveying methods, it does compare favorably with the accuracy of a third class survey.

A block diagram illustrating the overall method of operation of the device is shown in Figure 1. When the flashlamp in the optical transmitter breaks down, it triggers the sweep generator which causes a trace to appear on the cathode-ray tube. The light pulse transmitted by the lamp is returned from the distant reflector and falls on the cathode of the photomultiplier, causing a pip to appear on the trace. Upon returning to its normal state, the sweep generator triggers the second sweep timer which gates the range and trigger pip generators. These produce a second trace displaying range pips which can be matched with the target return pip by adjustment of the phase shifter.

When the second trace is completed, the circuits return to their normal states and are ready to repeat the cycle when the lamp flashes again.

A view of the instrument, assembled and ready for use, is shown in Figure 2. It will be noted that the instrument consists of four main units in addition to the batteries. These units will be called the optical head, the scope unit, the main power supply, and the converter unit.

The optical head is mounted on a tripod and contains the flashlamp and optical system necessary for transmitting the light from the lamp, receiving it from the distant reflector, and focusing it on the cathode of a photomultiplier. The output of the photomultiplier is amplified by a two-stage amplifier which is also part of the optical head and is contained, with the multiplier, in a chassis attached to the rear of the mirror housing. The optical head is mounted on bearings attached to graduated circles to permit its elevation and rotation to be determined within one minute of arc.

The output of the amplifier is fed through a delay line to the scope unit where it is amplified by two additional stages of amplification to produce the vertical "pip" on the cathode-ray tube. The scope unit is mounted on top of the main power supply during operation and can be tilted for the convenience of an operator seated in front of it. This unit contains, in addition to two stages of vertical amplification and the cathode-ray tube, the sweep circuits and other circuits necessary for the precise determination of range. A front view of the panel of this unit is shown in Figure 3.

The entire equipment is designed to operate from 110 volts, 60 cycle a-c or from 12 volts direct current. When d-c operation is desired, a 12 volt d-c to 110 volt a-c rotary converter contained in the converter unit is used. This unit also contains a neon sign transformer and rectifier to operate the flashlamp. The flashing rate of the lamp is controlled by varying the voltage fed to the primary of the neon sign transformer by means of a variac located in the main power supply. This unit supplies the plate and accelerating voltages to the tubes in the scope unit and in the optical head.

During operation the operator can turn the optical head by means of flexible shafts attached to cranks which fit on the scope unit. The electrical cables connecting the units during operation are stored in a compartment attached to the bottom of the scope unit when the units are packed for transportation. The sizes and weights of the units are such that they can be manborne.

Optical System

A schematic, cross-sectional view of the optical system is shown in Figure 4. The focal point of the parabolic mirror coincides with the center of the arc at point F so that an image of the arc is formed at infinity. The light returning from the target is focused on the pinhole or slit at point F' and then passes through the

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W. W. HANSEN is physicist with the Armour Research Foundation, Illinois Institute of Technology, Chicago, Ill.

Acknowledgment is made to the United States Army Engineer Research and Development Laboratories, under whose sponsorship and supervision the developments described were accomplished at the Armour Research Foundation; to the Radio Corporation of America, who developed special photomultipliers; to the General Electric Company and the Amglo Corporation for the development of flashlamps.

Credit is given to Bell Telephone Laboratories for their war-time work under National Defense Research Council contract resulting in basic developments in range measuring equipment utilizing the transit time of light pulses.

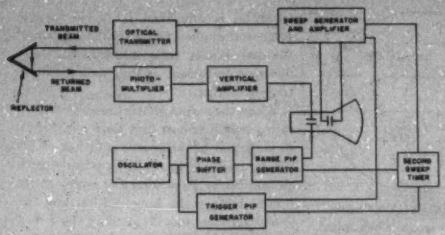


Figure 1. Block diagram of circuits

center of the parabolic mirror to the cathode of the photomultiplier. A slit is used at point F' for ease in "finding" the target when a search is being made for it; the pinhole is used for angular definition after the target has been found.

Ranging Method

The range accuracy requirements led to the adoption of a ranging scheme which involves the comparison of light transit time with the period of a crystal-controlled oscillator. An essential part of this scheme is a device which shifts the phase of the oscillator output to permit the matching of range pips to the target return pips. While no originality for this idea can be claimed here, its application to an optical transmitter involved special problems in synchronization. A similar scheme was used in the *AN/MPG-1* radar.[1] The process of pulsing a radio transmitter at a given instant is fairly straightforward and precise, but some concern was felt about the feasibility and possibility of triggering a gas discharge lamp within a very small fraction of a microsecond of a desired time, especially in view of the size and weight restrictions on the equipment. Instead of initiating the flashes of the lamp in synchronism with the oscillator, it would have been fairly easy to have started and stopped an oscillator in synchronism with the flashlamp but this usually entails some sacrifice in oscillator stability as compared to that of a free-running crystal oscillator and also introduces transient errors in the phase shifter.

These problems were avoided by the use of circuits which cause two traces to appear on the scope for each flash of the lamp. The first trace starts when the lamp flashes and displays the target return pip; the second trace starts about 0.01 second later and in synchronism with the oscillator. The second trace can be shifted vertically relative to the first one and displays a series of ranging pips which can be moved horizontally by means of the phase shifter until one of them matches the target return pip. The appearance of the scope screen when pips are matched is shown in Figure 5. The frequency of the oscillator is such that the interval between successive ranging pips corresponds to a range interval of 500 yards. A coarse range switch permits the interval containing the target pip to be brought to the center of scope screen where it is expanded to give greater

Figure 2. Assembled instrument, ready for use. Front view

Figure 3. Panel of scope unit. Main electronic unit (front panel)

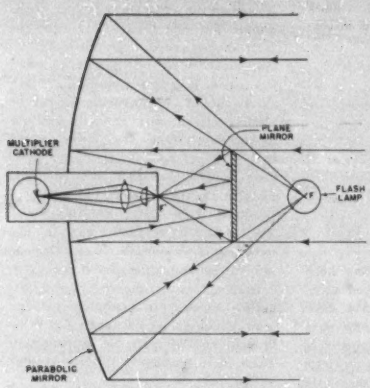


Figure 4. Cross section of optical system

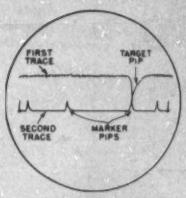


Figure 5 (above). Appearance of scope when pips are matched

matching accuracy. When pips are matched the range is the sum of the readings of the coarse range dial and the fine range dial. The coarse range dial is marked in steps of 500 yards; the fine range dial is attached to the shaft of the phase shifter and is graduated from 0 to 500 yards at intervals of one yard.

Sweep Generator and Sweep Amplifier

The schematic circuit diagram of the sweep generator and sweep amplifier is shown in Figure 6. To aid in the understanding of the operation of these circuits the flash lamp circuit is also shown in this figure although these components are not included in the scope unit. The sweep generator is a cathode-coupled flip flop circuit whose holding time is about 35 microseconds. The sweep amplifier is a cathode-coupled push-pull amplifier which is direct-coupled to the horizontal deflection plates of the cathode-ray tube. When the capacitor, C_1, is charged to a potential equal to that of the breakdown of the spark gap the lamp discharges it and, in doing so, applies a positive pulse to the left grid of T_1. This causes the normally conducting right half of the tube to be cut off for a time which is determined mainly by C_2 and R_1. When T_1 is triggered the potential difference across C_2 increases and causes the left grid of T_2 to become more positive. The change in potential across C_2 is considerably larger than that necessary to drive the left half of T_2 from cut-off to saturation, consequently the cathode spot moves across the cathode-ray screen only during a portion of the time that C_2 is charging. The time at which the sweep starts is determined by the setting of the coarse range switch which adjusts the bias on the left grid of T_2. If this grid were returned directly to ground, it would always be biased considerably beyond cut-off by the current flowing through the right half of the tube and the cathode resistor, R_2.

When T_1 is triggered the potential of its cathode drops abruptly. This change in potential is amplified by a deblanking stage and applied to the intensity grid of the cathode-ray tube, allowing the cathode spot to appear as long as T_1 is in its triggered state.

When the target is being searched for, a range-search switch (not shown in Figure 6) is turned to the search position. This places another capacitor in parallel with C_2 and biases T_2 so that the sweep starts at the instant the lamp flashes and the spot continues to move across the screen at a fairly uniform rate during the entire 35 microseconds sweep time. In this switch position, the second trace does not appear.

Second Sweep Timer and Trigger Pip Generator

When T_1 (Figure 6) returns to its normal state the potential of its cathode rises abruptly to about 50 volts and then drops rapidly to its normal potential of about 20 volts. This abrupt rise in potential is used to trigger the second sweep timer which is a flip flop circuit with a holding time of about 0.01 second. The schematic circuit diagram of the second sweep timer and the trigger pip generator is shown in Figure 7. The output of the os-

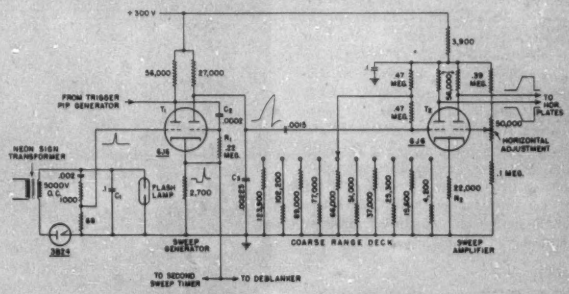
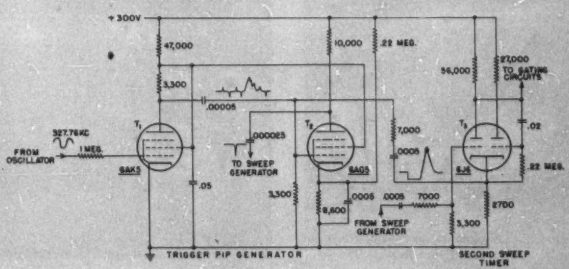


Figure 6 (above). Schematic circuit of sweep generator and sweep amplifier

Figure 7. Schematic circuit of second sweep timer and trigger pip generator



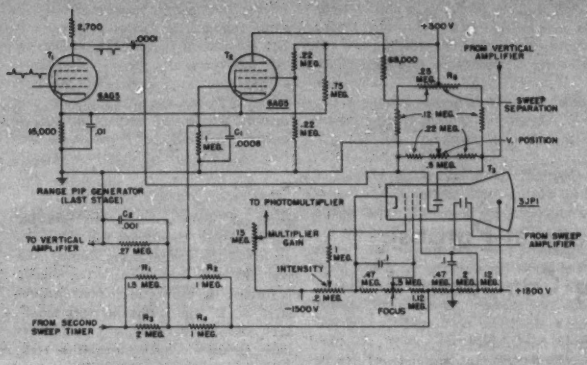


Figure 8 (above). Schematic of gating circuits

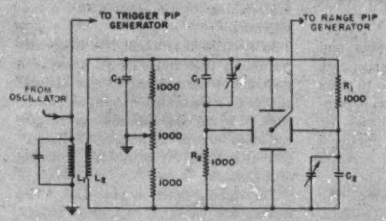


Figure 9. Schematic of phase shifter and phase-splitting circuits

cillator is sufficient to overload T_1, causing its output to approximate a square wave. This is differentiated so that the signal appearing at the grid of T_2 is a series of alternately positive and negative pulses. Tube, T_2, is normally biased sufficiently to prevent any signal from appearing at its plate. However, when T_2 returns to its normal state its cathode potential rises abruptly, producing a pedestal voltage at the control grid of T_3 and allowing a pulse to trigger the sweep generator and initiate the second sweep. When the sweep generator returns to its normal state it does not trigger the second sweep timer again because the cathode potential of the second sweep timer does not drop sufficiently during the 35 microsecond second sweep interval.

Gating Circuits

When the second sweep timer is triggered at the completion of the first sweep, the drop in potential of its left plate is used to actuate gating circuits which shift the second trace vertically relative to the first and allow marker pips to appear on the second trace. These circuits also cut off the vertical amplifier during the second trace to prevent noise from appearing on it. The schematic diagram of the gating circuits is shown in Figure 8. The cathode-ray circuits are shown for clarity but only a portion of the range pip generator is shown because it is similar to the trigger pip generator except for an additional stage of amplification to compensate for the phase shifter attenuation.

The values of the resistors, R_1 and R_2, are chosen to apply a positive potential to the grid of T_2 when the second sweep timer is in its normal state. The cathode current of T_2 biases T_1 beyond cut-off so that no signal appears at its plate. The plate current of T_2 flows through the center-tapped potentiometer, R_5, and produces a difference in potential between the vertical deflection plates of T_4 whose magnitude and sign depend on the position of the slider relative to the center-tap and thus determines the position of the first trace relative to the second. When the second sweep timer is triggered, tube T_2 is biased beyond cut-off. This releases some of the bias on T_1 and allows a signal consisting of a series of negative pulses to be applied to one of the vertical plates of T_3 and cuts off the plate current which flowed through R_6 during the first trace. Capacitor C_1 insures that tube T_2 remains cut off during the second trace.

Resistors R_3 and R_4 are chosen so that the final stage of the vertical amplifier is biased beyond cut-off during the second trace but is biased normally during the first. Capacitor C_2 insures that the vertical amplifier remains cut off during the second trace.

Phase Shifter

The schematic circuit diagram of the phase shifter and of the phase spitting circuits which supply it is shown in Figure 9. The phase shifter is a special capacitor consisting of four quadrants which induce voltages in a common pickup plate which faces them. The theory of operation and a discussion of the errors of the phase shifting capacitor are given in other publications.[2,3] These capacitors are made by the P. J. Nilsen Company, Oak Park, Ill. An eccentrically mounted, circular, mycalex disk is mounted between the quadrants and the pickup plate so that the capacitance between each quadrant and the pickup plate varies as the disk is rotated. One mechanical degree of shaft rotation produces one electrical degree of phase change in the vector sum of the voltages induced in the pickup plate provided the quadrants are supplied with voltages of equal amplitude but in phase quadrature. This situation will exist if the reactances of capacitors C_1 and C_2 (Figure 9) are equal to the resistances of R_1 and R_2 at the oscillator frequency and if the network is balanced to ground. The capacitor, C_3, provides a balance to ground by compensating for the interwinding capacitance between L_1 and L_2.

Vertical Amplifier

The vertical amplifier has a voltage gain of about 85 decibels and is shunt compensated to a top frequency of two megacycles. A delay line between the second and third stages serves as a flexible coupling between the optical head and the scope unit and delays the signals sufficiently to allow range readings to be made on very close targets. In fact, the zero adjustment of the phase shifter shaft is made by holding a reflector next to the framework of the optical head and in the light beam. This permits a ready determination of the zero correction in the field.

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No Discussion

A Large-Scale General-Purpose Electric Analog Computer

E. L. HARDER
MEMBER AIEE

G. D. McCANN
MEMBER AIEE

OVER the past ten years computation by the electric analog method has come into increased use in the solution of transient problems in the fields of electric circuits[1,2,3] and machinery, and in applied mechanics,[4–9,12] hydraulics, and heat flow.[5] It has also been found admirably adapted to the study of electromechanical systems such as servomechanisms[10,11,13,15] and controls.[14] These developments have been reported in previous papers before the Institute and elsewhere.

A little over two years ago a survey was made to determine the general field of problems which could be solved by the electric analog method, special emphasis being placed on the design and application of electrical equipment. Both electrical and mechanical problems were considered. At the same time a study was made of the various other types of computers both present and proposed which might be applied to the solution of these problems. As a result of this survey, it was decided that the electrical analog computer was best adapted generically and economically to the scope of problems under consideration and it was decided to build a general purpose computer along these lines which would be able to handle a great majority of these problems.

Generally speaking, the new computer was to incorporate the facilities of the electrical[1] and mechanical[8] transients analyzers, and the servoanalyzer[12] previously described, together with new facilities to form a general purpose computer. The new facilities included principally multipliers, transformers of special design, and forcing functions of the independent variable as well as non-linear functions of the dependent variable.

It was decided to redesign all existing elements to eliminate weaknesses and limitations found through experience ranging from a few months up to 10 years on various parts of the small-scale computer. Furthermore, the facilities for connections should be greatly improved and control desks designed to expedite measurements and give the operators continuous control of elements requiring frequent change throughout a problem. For economy of loading two control desks were to be provided and the facilities arranged for simultaneous solution of two or more problems.

Several months after the design of the Westinghouse computer was undertaken, arrangements were made for a similar computer to be installed at the California Institute of Technology, and for further development work to be shared.

The resulting two computers are alike in all essential features and general design, differing only in minor matters of construction and in the exact number of each kind of element installed. These differences are dictated largely by the principle uses contemplated for the respective computers.

Description of the Computer

1. Principle of Operation

In order to understand the purposes of the various elements, it will be necessary to outline briefly the operation of an electric analog computer. For a more detailed exposition of the theory the reader is referred to previous papers and articles listed at the end of the paper, particularly references[1,4,8,12]. These papers give the general theory of the repetitive method of solving transient problems, and the theory of analogs and their use in electrical computers. As shown in Figure 1 the computer can be divided into three principle parts, namely, the forcing functions, the analog of the system being studied, and the measuring equipment. The analog is formed by connecting circuit parameters, R, L, and C, together with amplifiers and transformers and multipliers into a circuit which has the same differential equation as the system under consideration.

In a transient problem, it is frequently desired to determine the response of a system to some applied force or disturbance or to some change in a circuit parameter. The forcing function must generate this force in the form of a voltage or current and apply it to the electric analog of the system.

The forcing function may be applied repetitively by a synchronous switch. Thus the solution, which also repeats, can be displayed on a cathode-ray oscillograph for visual or photographic measurement.

Typical steady-state and transient problems are illustrated in Figures 1(c) and (d). The steady-state problem is a torsional vibration analysis. The forcing function is then a variable frequency power source, representing possible exciting forces. The mass-inductance analog is used in which voltage corresponds to torque and charge to angular displacement. The susceptance of the capacitor is proportioned to the spring constant of the shaft.

The solution in this case is expressible as torque build-up. By measuring the ratio of capacitor voltage to input voltage by cathode-ray oscillograph and attenuator or by a voltmeter, the ratio of shaft torque to input is obtained.

In the transient problem Figure 1(d) the electric torque exerted at the air-gap

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E. L. Harder is consulting transmission engineer, industry engineering department, Westinghouse Electric Corporation, East Pittsburgh, Pa., and G. D. McCann is professor of electrical engineering, California Institute of Technology, Pasadena, Calif.

of a turbogenerator during short circuit is a complex wave. It contains three components of different frequencies and decrement. These are generated by capacitor discharge in oscillatory circuits of corresponding frequencies. The resulting currents are passed through a common slide wire resistor to obtain a "forcing function" voltage having the required shape.

The analog is the same as for the steady-state problem. However, the measuring equipment in this case consists of cathode-ray oscillograph. Maximum torque in the shafts can be read visually or the entire plot of torque versus time photographed. Figure 2 shows the wave shapes involved.

These sample cases illustrate the principle of operation. In practice many of the analog circuits are more complicated, some requiring several hundred circuit parameters for adequate representation. Many different types of forcing functions are required. Special transformers, amplifiers, multipliers and nonlinear functions are needed for the analog elements. Various metering methods are required depending on the nature of the problem. Also a rapid and flexible arrangement is needed for connecting and checking the circuits and metering all parts of the problem. The subsequent paragraphs will outline the general arrangement which has evolved and describe the various elements and the application of the computer.

2. General Arrangement of the Computer

The general arrangement of a large scale computer is dictated by the considerations of maximum utility and flexibility and minimum operating difficulties and maintenance. This requires consideration of shielding, and of the length of leads. It requires provision of flexibility for handling a large variety of problems and accessibility of all parts for maintenance. In the case of a two-desk computer the least possible interference should be occasioned when two problems are going on simultaneously. On the computer a wide variety of impedances and frequencies are involved. For this reason, particular attention must be given to the shielding and stray capacity and inductance effects. The new computers have been designed to provide an optimum arrangement with all of these factors considered.

The number of units of each type required for a general purpose computer is dictated by economics, by experience, and by forecasts of requirements for the general class of problems which it is expected to handle. The compositions of the Westinghouse and California Institute of Technology's computers is given in Appendix I.

The general arrangement of the computers is indicated in Figure 3. The Westinghouse computer is located in a room adjacent to the a-c calculating board to provide the benefits of interchange between the transient and steady-state computers. The circuit element cabinets are arranged along one wall of the computer room, interspersed with plugboards upon which the connections are made to form the analog and forcing function networks. An electronic power supply cabinet is located in the center of this row of cabinets, while the 440-cycle power supply, being noncritical to lead length, is located along the opposite wall.

Amplifier and multiplier cabinets are at one end of the room adjacent to metering desk 1. This desk will normally handle the servo and control system type of problems. Desk 2 at the other end of the computer room will normally handle power system problems, using the 440-cycle supply, as well as transient torque problems, and others not requiring the electronic equipment. However, the amplifier cabinets have been made portable so that they can be divided in any desired manner between the two metering desks. One or more of the cabinets can be moved physically to the location of the second desk in order to make electronic analogy set-ups without extensive leads.

Time delay elements consisting of variable resistance and capacitance are included in the several amplifier cabinets so that servo and control system problems can in critical cases be set up entirely within the confines of the amplifier cabinets.

The California Institute computer differs from the Westinghouse unit in composition, as indicated in Appendix I. Also its arrangement is somewhat different. Figure 5 shows a general view of the California Institute's computer. Since it is not to be operated in conjunction with a separate a-c calculating board, it has been designed so as to be suitable for electric power system problems. At present only one metering desk and one forcing functions cabinet is in use. However, the heavy schedule under which it has been operating indicates the need of an additional control center. One more metering desk and forcing functions cabinet will probably be added. Twenty-five main busses run throughout the computer. These are sectionalized at the two corners, Figure 3(b), so that the three sides can be used separately for simultaneous setting up two or more problems. However, all of the general development, the component element electrical designs, the principles of shielding, the power levels, ranges, accuracies, wiring and selection principles are the same for these two computers.

The general wiring plan of the Westinghouse computer is shown in Figure 4. Circuit parameters, R, L, and C, as well as

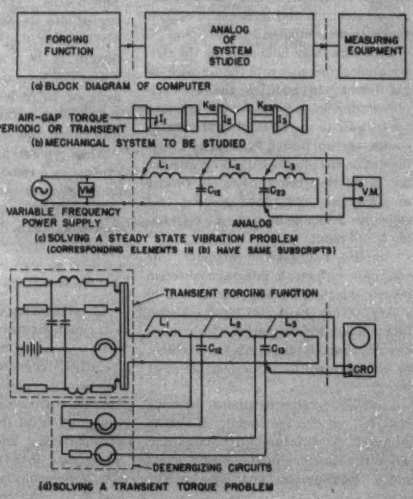


Figure 1. Schematic of analog computer

(a). Block diagram
(b). Mechanical system
(c). Torsional vibration analysis
(d). Transient torque analysis

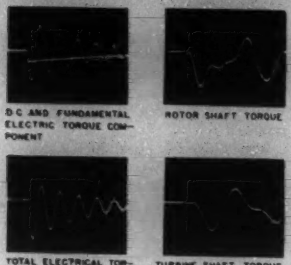


Figure 2. Transient torques in shaft and coupling due to a line-to-line short circuit of a generator

transformers and all power supplies are brought out to plugs or receptacles on the interspersed plug-boards. This is indicated by the lines from the *A* cabinets, containing these elements, to the plugs indicated by arrows on the front of the plug-board cabinets, *B*. Connection panels, on the plug-boards *B*, are provided with receptacles (not shown) arranged in rows and so wired that all plugs in adjacent receptacles are automatically connected together. Analogs are made up by plugging together the appropriate circuit elements on the plug-boards.

To provide flexibility for the use of remotely located elements of the computer, a 48-wire relay bus runs to all plug-boards. A 20-wire bus to the development area provides flexibility for development of additional forcing functions and circuit elements and their use in connection with the computer.

A 60-wire relay interconnects the electric analog computer with the a-c calculating board. In addition the 440-cycle power supplies, cabinet *J*1, are arranged for transfer to use either with the analog computer as indicated by terminals coming out on the plug-boards, *B*, or with the a-c calculating board. Remote control of magnitude and phase position (not shown) is available from the a-c calculating board room.

Amplifier-multiplier cabinets are equipped with small integral connection boards which minimizes the extent of wiring. They have also 20-wire busses brought out to receptacles which interconnect through plug cords from cabinet to cabinet and to the circuit element board.

From his operating location at metering desk *D*-1, or *D*-2, the operator is able to adjust the synchronous switch stages, and select the locations in the analog across which oscillographs and synchronous switches are connected. He can meter any analog circuit, insert and adjust damping resistors, control magnitudes of key parameters, and control the forcing functions.

These facilities are made available by a variety of control and circuits indicated schematically in Figure 4. Synchronous switch to bus, selectors, and damping resistors are mounted in cabinets integral with the synchronous switches, H-1, H-2 and H-3. The other controls and instruments are part of the metering desk.

Each desk contains two fifteen-circuit busses which terminate on the main plug-boards and are also carried through plug cords to the synchronous switches. Essential analog and forcing function points are connected to these busses so that oscillographs or synchronous switches can be connected thereto by the selector switches.

A four-wire meter bus runs from each desk throughout the length of the computer. Relay-type selectors, controlled by keyboards on each desk, cut the meter bus into any desired circuit element of the computer. At the desk, either electronic or 440-cycle vector meters can be selected on this metering bus, or the meter bus can be selected by oscillographs or synchronous switches. The latter connection gives wider coverage to these facilities, and is particularly useful in power system switching transient studies.

Each desk contains four decade resistors, four adjustable inductors and two adjustable capacitors whose terminals appear directly on the circuit elements plug-boards. These elements can be connected into the analogs the same as any of the other units but can be adjusted directly at the desk.

One synchronous switch is permanently assigned to each desk while the third switch is movable and can be plugged into either desk as desired.

This general arrangement has been evolved as a result of design calculations and experience with shielding and measurement problems during several years experience with the smaller scale computer.

3. Steady-State Forcing Functions

Physical systems for which the differential equations can be written are amenable to solution by computers. Consequently the computer nomenclature parallels that of analytical solution of the differential equation, or system of equations. For example, in the differential equation of a particular linear system, $y(p)=f(t)$, where $p=d/dt$ the right-hand side $f(t)$ is the "forcing function" of the independent variable, t. Usually, the forcing function equipment of the computer must enforce the restraint corresponding to the application of this known force. If it is desired merely to reveal the natural frequencies and damping rates the forcing function may be very simple, such as unit function. However, many of the forces to which a physical system may be subjected are complicated functions of time. Considerable flexibility is required for a computer to produce almost any desired forcing function. In general, these may be divided into steady state and transient forcing functions. The facilities provided on the new electric analog computer for steady forcing functions are the following.

Variable Frequency Units. Four electronic power sources are provided in the Westinghouse computer, six in the California Institute's computer. In each case these are operated synchronously from a single master oscillator but are separately adjustable in phase and magnitude. These sources are single phase, 0 to 50 volt, and cover the frequency range from 100 to 1,600 cycles. A unique phase angle adjustment is provided which is relatively independent of frequency, over nearly four to one frequency range. Thus, with out-of-phase exciting force applied to a system, the response as a function of frequency can be obtained by varying the oscillator without changing the phase positions of applied forces. This forcing function is shown diagrammatically in Figure 6.

The sinusoidal forcing functions for the California Institute computer are designed for 50 volts at one ampere to increase their usefulness for electric power system problems.

Constant Frequency Units. On the Westinghouse computer four 440-cycle power sources are provided similar to those used on the a-c calculating board.[16] A synchronously driven motor-generator set supplies the 440-cycle power. Each "source" consists of a phase shifter for adjusting the phase angle and a pair of oppositely rotating phase shifters for controlling magnitude. These sources are single phase, 0 to 220 volt, 0 to 3 ampere and provide for operation at a higher power level where variable frequency is not required.

Direct Current Supplies. Several d-c power supplies are available, namely lead cell storage batteries in steps up to 48 or 72 volts, heavy duty *B* batteries up to 500 volts 250/500 volt regulated power supplies, 0 to 500 mils and several others. All variable frequency power supplies, amplifiers and multipliers have integral

shielded d-c power supplies. The shielding is discussed later.

4. Transient Forcing Functions

As a majority of the transient computations are performed by repeating the transient, the forcing functions are arranged to provide this repetition. Two general forms are used.

General Arbitrary Function of the Independent Variable. Three units of this type are installed and are illustrated in Figure 5 (drum to right of desk) and in Figure 7. The voltages of 100 consecutive bars of the 200 bar commutator are separately adjustable by means of the 100 stage drum-type selector switch. The other bars are connected together for the "off" period. As a brush sweeps around, it picks off the desired function of time (the independent variable). This can be used as a voltage or converted into a current by means of the current forcing function (described later), depending upon the needs of the problem. In addition to the arbitrary voltage forcing functions mentioned before, the California Institute computer also has two phonograph-type pick-up circuits that play specially prepared records to generate any desired voltage function.

Current Type Forcing Functions. Electronic amplifiers or current regulators are provided which can force a current to follow a prescribed voltage pattern. By this device practically any forcing function which can be generated as a voltage can be converted to a current type of forcing function, if this is required for the analog used. In the California Institute computer, for example, three of these constant current generators are provided. They employ pentodes that will supply up to 50 mils through impedances up to 100,000 ohms. These can be driven from any of the voltage sources so as to provide any form of current function.

Synchronous Switch. A large variety of transient forcing functions can be produced by means of the synchronous switch used in conjunction with power sources, circuit parameters, amplifiers, and other computer elements.

The synchronous switch, shown in Figure 8, is an eight-stage-rotating drum-type switch, each stage of which is separately adjustable in phase and duration of closure. It is driven by a special synchronous induction motor usually at 600 rpm but having gears for 1,200 and 1,800 rpm. Because of its mass and the synchronous operation, the speed is very steady and a high degree of switching definition is obtained, for accurate repetition. The stages are indexed to facilitate returning readily to a previous phenomenon. Low brush drop is achieved by special alloy drums and silver–graphite brushes.

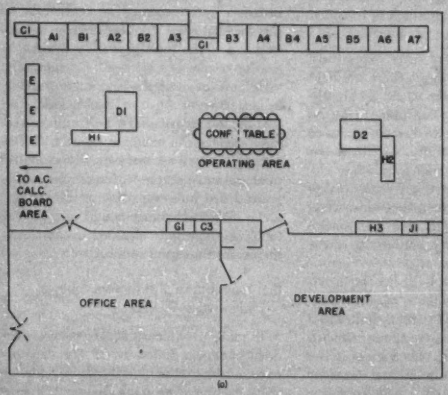
The 30-bus selector is mounted integral with the switch. When the analog points of interest are connected to these 30 busses the switch stages can readily be switched to the desired locations.

The simplest transient forcing function is, of course, simply on-off switching. Unit function can be made up by means of a low-impedance battery shorted through a resistor by a switch stage. When the switch opens each cycle, the battery voltage is applied. As one ampere can be readily commutated, a 6-ohm resistor can be used with 6-volt battery, providing six ohm effective internal impedance during the open period and zero during the closed period.

A more complex transient forcing function produced by d-c power source, oscillating circuits and synchronous switch is shown in Figure 2. The capacitors are charged during the open period through high resistance and are simultaneously discharged by closure of the switch stage to produce the desired forcing function. The currents are combined in appropriate portions of the slide wire resistor to form the forcing voltage. Independence of analog and forcing function circuits is secured by low impedance of the slide wire, and by low voltage across the slide wire compared with the oscillating forcing function circuits.

Spliced Waves. The synchronous switch also provides rich possibilities in the "splicing" of sections of waves from different sources to form a desired forcing function. A sloped front, flat topped wave is formed by using the first 20 or 30 degrees of a sine wave and switching to a battery. For studying rectifier and inverter wave shape problems the switch stages perform the periodic switching functions in the circuit normally performed by the rectifier valves. Angles of overlap and grid delay or advance can be readily controlled.

De-energizing Circuits. The de-energizing circuits are secondary or less critical forcing functions used to remove energy from the analogy during the "off" period, restoring it more quickly to the correct initial boundary conditions for reapplication of the transient phenomena. They consist generally of damping resistors applied at appropriate times by synchronous switches.



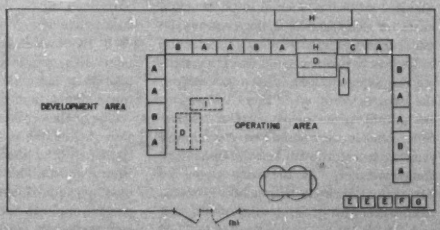


Figure 3. General arrangement of Westinghouse and California Institute of Technology computers

(a) (left). Plan of Westinghouse Electric analog computer (Refer to Appendix 1A for letter symbols)

(b). Plan of California Institute of Technology analog computer (Refer to Appendix 1B for letter symbols)

5. Analog Elements

The analog is the heart of the computer. This portion comprises all of the basic elements, resistors, inductors, capacitors, transformers, amplifiers, and multipliers. The accuracy of the computer hinges on the maintenance of high standards of accuracy. This requires linearity, permanence of calibration, and minimizing the effects of leakage and stray fields. Extensive developments have been carried on to improve the basic analog elements and to eliminate sources of error found through experience on the smaller scale computer.

High Q Inductors. Inductance coils have been developed having a Q of 100 or higher over the frequency range from 100 cycles to 1,000 cycles, and having inductance values and taps as indicated on Appendix I. It was desirable to make these inductors as small as possible as they are one of the major space consuming items of the computer. They also represent a considerable item of cost. Coppernik F laminations of five mils thickness were selected as most suitable for this application. The air gap is at the center of the core which minimizes fringing and interference between inductors. Both theoretical and experimental analyses were carried out to find the best proportions for general computer purposes.

Capacitors and Resistors. Precision capacitors and resistors were used for all of the variable units of the new computer to eliminate the necessity of bridge measurement or trimming of units used in problems requiring the higher degree of accuracy. Resistors and inductors were in general combined into R-L circuits. Each such circuit is brought to a single pair of plugs and requires a single metering relay. Either parameter can, of course, be set at zero. No problem could be foreseen requiring large numbers of both resistors and inductors, other than in this series combination.

Special Transformers. In certain mechanical problems, such as impact loaded beam for example, the analog requires the use of transformers in order to enforce the restraints between certain currents without requiring metallic interconnections of the circuits involved. These transformers should be as nearly perfect as possible; that is, they should have minimum exciting current and leakage reactance and resistance. At the same time, the size should be kept within bounds commensurate with general computer design and cost.

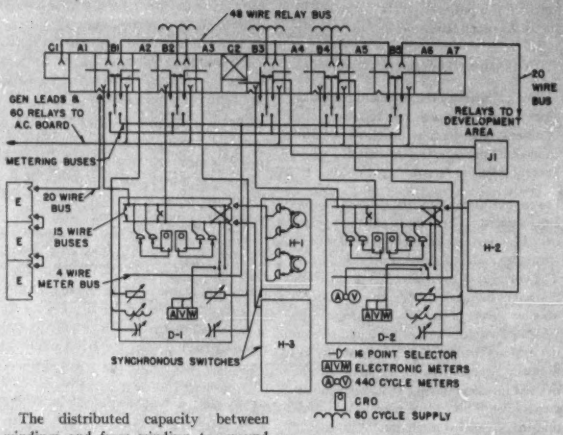


Figure 4. Wiring plan of Westinghouse electric analog computer (For letter symbols refer to corresponding letters in Appendix IA)

The distributed capacity between windings and from winding to ground should be as low as possible. In general, leakage reactance can be minimized by interleaving at the expense of increasing the winding-to-winding capacity so that an optimum compromise has to be developed between these two conflicting requirements. Five-mil hydrifined molybdenum permalloy bar piece laminations were selected for the cores of these transformers. The resulting units have a ratio of exciting impedance to leakage impedance of 1,400 and winding-to-winding capacitance of 400 micro-microfarads.

Time Delay Units. Time delay units are adjustable resistance-capacitance circuits used primarily on conjunction with amplifiers to simulate the time constants of various stages of control and servomechanism systems. Internal resistance of the preceding amplifier stage is included in any accurate work.

Amplifiers. Amplifiers enter extensively into the analogs; without them the field of the analog computer would be greatly restricted. Circuits can be arranged which are analogous to certain parts of the system under study. Voltages or currents may be derived from these circuits without loading them or distorting their theoretical performance and these quantities amplified in turn to provide relatively large power sources to drive other elements of the analog.

One of the problems has been the provision of shielding suitable to permit operation of the amplifier at potentials elevated from ground. This has been accomplished by a specially shielded transformer in the power supply to each individual amplifier and by other measures. The complete amplifier chassis is insulated from the supporting frame and can be used at elevated potentials when required in the analogy. One of these units is shown in Figure 9.

Multipliers. In the multiplier even more than in a simple amplifier, isolation becomes one of the major problems. Two general designs of multipliers have been adopted for the computer. One, devised by R. C. Cheek, uses a carrier signal. Figure 10 shows the carrier-type multiplier.

The second type uses a mixer-type multigrid tube. This arrangement is somewhat simpler and can be used where isolation is not required, as in the case in certain analogies. A particularly important use of the multiplier is in the provision of square law elements for representation of elements of a hydraulic problem or gas flow problem. In this case, the two inputs may of course be common and may also have a common point with the output so that all sections of the multiplier can be operated with a single chassis ground tied into one point of the analog. Even with this arrangement, it is essential to have a specially shielded input transformer for the power supply.

6. Nonlinear Functions of the Dependent Variable

In many problems it is necessary to produce nonlinear functions of the dependent variables, that is, of the currents or voltages appearing in the analog. Several

Figure 5. General view of computer installed at the California Institute of Technology

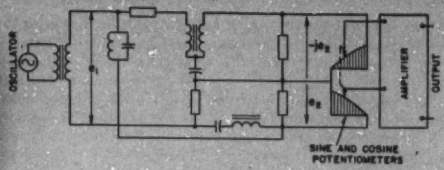


Figure 6. Variable frequency forcing function

devices have been developed to produce such functions. Diode or rectifier circuits have been built up to simulate many of the simpler functions. For example, the discontinuous current and voltage limiting circuits have been so obtained.

The California Institute computer has two elements that can generate arbitrary, single-valued functions of a dependent variable, and more are being constructed. They employ a cathode-ray oscillograph with a very short persistence screen. It is swept along one axis at a constant high frequency rate and along the other axis by the dependent variable. A mask is placed over the screen to represent the desired function and the light from the screen picked up by a photocell and amplified.

Multipliers and arbitrary functions of the dependent variable are extremely important components of the computer since they extend its application into the general field of nonlinear problems.

7. Measuring Equipment

Several types of measuring equipment are used in connection with the computer. These include cathode-ray oscillographs, cameras, harmonic analyzers, and all types of conventional a-c and d-c ammeters, voltmeters and wattmeters both indicating and recording.

Horizontal time sweeps are obtained either using the internal sweep circuits of the cathode-ray oscillograph or, where greater accuracy or longer scales are required, the use of a drum or disk type rotating film holder is used. This is driven synchronously so that by repeating the transient the equivalent of a very high writing speed can be obtained. The synchronous operation and suitable inertia also provides the required linearity of sweep.

Metering Desk. Figure 5 shows the general arrangement of instruments and controls on one of the metering desks. The oscillographs are mounted directly in the center, with their 15-bus selector switches at the right. A fixed focus camera slips over the front of the oscillograph for photographic recording of data.

The upper panel also contains voltmeter, ammeter, and wattmeter operated through electronic metering amplifiers to register these quantities over the frequency range from 30 to 2,000 cycles. At the left is the oscillator controlling the variable frequency forcing functions.

The other controls are power switches, circuit selector keys, miscellaneous selector switches, and adjustments of the desk mounted R, L, and C parameters. The vector 440-cycle meters and the phase shifter for controlling the reference current are mounted on a sloping panel on one desk of the Westinghouse computer.

Portable Measuring Equipment. Practically all types of indicating and recording instruments used in general power system work can also be used in special problems to make measurements on the computer. More frequently used devices are made part of the computer, whereas others are shared with other laboratories. A few examples will be mentioned.

Harmonic Analyzer. In rectifier and inverter problems the synchronous switches are used as mechanical rectifiers, the angle corresponding to overlap and ignitor delay or advance being readily adjustable. The resulting wave shapes are analyzed by harmonic analyzer, just as they would be on an actual system. This instrument is also useful in the steady-state saturation problems such as ferroresonance.

Wire and Tape Recorders. The wire recorder may of course be used for storing solutions of a noncritical character, or solutions in which wave shape, or damping rates are of chief concern. A recording of from a few seconds to a minute of a repeated wave form would usually be sufficient for later examination.

The transient tape recorder, operating with a short continuous belt of recording tape, makes it possible to play back repetitively a "single shot" transient solution. This technique is useful when restoration of the initial boundary conditions is difficult with the repetitive method.

Both the wire and tape recorders offer possibilities as forcing functions as well. Some development work has been carried on along this line. However, other methods appear more direct for the present.

8. Shielding

The difficulties of using a cathode-ray oscillograph at elevated potentials are well known. The possibilities of cross talk or pickup at audio frequencies in an extensive computer operating at these frequencies are also quite apparent. Consequently a large part of the development of equipment and techniques required to insure accuracy must be directed toward elimination of these undesirable stray effects.

A complete résumé of the shielding considerations is entirely beyond the scope of this paper. The following discussion will serve to outline the general problem and indicate the nature of the solution.

Stray effects are of two general types, namely, those which introduce voltages into the analog, and those which change the value of parameters. In general these effects are resistive, inductive, and capacitive. All are important in certain cases.

A stray voltage of 1 per cent induced into a circuit may be amplified 100 times if it happens to be at the resonant frequency of an L-C circuit having a Q of 100. A stray capacitance which appears utterly unimportant at the working frequency may oscillate with a coil in a system of high amplification, obscuring the solution.

A leakage resistance 100 times any of the analog impedances may be thought unimportant. If it terminates in an extraneous voltage source 100 times as large as the voltage in that part of the analog, the stray current may be equal to the working current in the analog.

Extreme cases have been cited which would result in gross errors or nonoperation. However, shielding methods and techniques have been developed which keep these effects well within the limits necessary for good engineering accuracy. This has been accomplished by capacitive and magnetic shielding, by design arrange-

ments that eliminate the sorts of leakage paths cited, and by minimizing capacitance to ground or between wires or devices.

For example lead capacitances have been reduced approximately ten-to-one by the use of spaced bare wire instead of bundled conductors. Inductance coils are arranged with the gap in the center leg, thereby minimizing stray fluxes and effectively shielding the coil from extraneous fields. Optimum use has been made by the shielding possibilities of the steel enclosures.

9. Combined Functions of A-C Calculating Board and Analog Computer

The Westinghouse analog computer is located adjacent to the a-c calculating board and interconnections are provided to permit the facilities of either calculator to be used in connection with the other. This procedure greatly increases the utility of both facilities. The a-c calculating board includes 200 resistor-reactor units, 48 capacitor units, 18 low-loss reactors, 78 transformers and 18 440-cycle powers sources. All of these units are available at certain times for extensive problems on the analog computer. This arrangement has been in use for several years with the small computer. It has proven most advantageous, particularly for power system switching problems and other electrical transients.

The Q of the a-c calculator coils is not sufficiently high for many of the mechanical system transient problems. It is approximately 25 at 440 cycles compared with 100 or higher over the full 100 to 1,000 cycle range for the coils associated with the analog computer. However a Q of 25 is quite adequate for most electric circuit problems and many others.

The interchange operates in both directions. The four 440-cycle power sources of the analog computer together with the 144 reactor and resistor-reactor units, the 124 capacitor units and 56 transformer units can be used at certain times in connection with the a-c calculating board problems. Sixty-eight interchange circuits are provided initially to take advantage of this flexibility.

Application and Conclusions

The list of problems given in Appendix II illustrates the general field of application of the electric analog computer. However, as titled, this list is purely suggestive rather than complete. The list grows almost daily as one new problem after another is found to be amenable to study on the computer. The capabilities of the computer suggest other problems of importance that can be solved. The urgent need for solutions of still other problems suggest developments or improvements that permit solving those problems and which extend the application of the computer into new fields.

The goal in this development has been the production of a very flexible general purpose computer, one which would anticipate most of the requirements of elements and connections for analog solutions of the problems listed in Appendix II. This has required elements to simulate all the terms occurring in the differential equations of the systems to be studied. Linear circuit elements and amplifiers represent all the constant coefficients. Multipliers provide for multiplying variables. Arbitrary functions of both independent and dependent variables are provided. Accuracy depends on the problem being solved. Generally speaking, acceptable engineering accuracy of one-to-five per cent is secured for the type of problems being solved. The parameters can usually be set more closely than the data on the physical system is known, and the results are therefore nearly as accurate as the available data could produce.

The computer described has been

Figure 7. California Institute of Technology electric analog computer

View of forcing functions cabinet which contains 18 forcing functions that can apply either sinusoidal or transient voltages and currents to the electric circuits which simulate the mathematical problem

Figure 8. Eight-stage synchronous switch used for forcing functions, for de-energizing circuits and for synchronizing the oscillograph

Figure 9. Amplifier, completely isolated and shielded and having self-contained power supply

Figure 10. Multiplier, carrier type, for producing the product (or quotient) of two functions, represented as voltages

evolved as a result of design calculations. coupled with experience obtained during several years operation of a smaller scale computer. In spite of its limitations a wealth of useful data has been obtained from the small scale computer. With the enlarged facilities, the improved shielding, the reduction of stray effects, the greater accuracy, and the newly developed components, the horizon of the new computer has been greatly expanded.

Appendix I

A. Basic Elements of Westinghouse Electric Corporation Analog Computer*

A. Seven Passive Circuit Elements Cabinets

1. 100 high Q inductors—0 to 0.75 henry in 0.001-henry steps.
2. 20 high Q inductors—0 to 1.5 henry in 0.1-henry steps.
3. 20 high Q inductors—0 to 0.75 henry in 0.05-henry steps.
4. 80 resistors—0 to 8,000 ohms in 2-ohm steps.
5. 10 resistors—0 to 111,000 ohms in 200-ohm steps.
6. 60 capacitors—0 to 1.1 microfarad in 0.01 microfarad steps.
7. 40 capacitors—0 to 4.1 microfarad in 0.01 microfarad steps.
8. 24 capacitors—10, 20, 40, or 80 microfarad fixed.
9. 55 special transformers.

B. Five Plug-boards

1. Plug leads for all elements of the seven circuit element cabinets, three forcing function cabinets, and a-c calculating board interconnections.
2. Receptacle patch boards for circuit element connections, and terminations for main busses, amplifiers, multipliers, analog selector busses and relays to development area.

C. Forcing Function Cabinets

1. Four sinusoidal variable frequency and phase position voltage sources (0 to 50 volts).
2. Two separate square wave voltage sources (0 to 12 volts, or one 24-volt source in 2-volt steps).
3. Three arbitrary voltage sources (maximum 50 volts, one ohm).
4. Two arbitrary current sources (controllable from any voltage source).

D. Two Metering Desks—Each Desk

1. Three meters for metering sinusoidal audio frequency current, voltage, power.
2. Two cathode-ray oscillographs for transient solutions.
3. One 4-wire metering circuit (and selector system) for metering all passive circuit elements and source forcing functions.
4. Two 15-circuit analog selector busses.
5. Seven adjustable R, L, C circuits with terminations on plug-boards (4 R, 1 L, 2 C).
6. Master oscillator for sinusoidal variable frequency forcing functions.
7. Slide wire (6 slide) for combinations of forcing functions.

One Desk

8. Vector ammeter and voltmeter for 440-cycle metering.

E. Amplifier—Multiplier Cabinets (portable)

1. Twenty amplifier units, each having an RC time-delay circuit for servo problems.
2. Six multipliers with isolated inputs. (Part under construction.)

F. Arbitrary Functions of Dependent Variable (portable)

1. Two voltage limiters.
2. Two arbitrary function devices (single valued.)

G. Synchronous Switches

1. Three 8-stage synchronous switches (one portable).
2. Integral 16-circuit selectors for analog busses.

H. Constant Frequency Forcing Function

1. Four 440-cycle 250-volt 3-ampere sources adjustable in phase and magnitude. Control local or remote from a-c calculating board.

B. Basic Elements of the California Institute of Technology Electric Analog Computer†

A. Ten Passive Circuit Elements Cabinets

1. 100 high Q inductors—0 to 0.90 henry in 0.001-henry steps.
2. 20 high Q inductors—0 to 1.50 henrys in 0.01-henry steps.
3. 100 resistors—0 to 6,000 ohms in 2-ohm steps.
4. 20 resistors—0 to 150,000 ohms in 200-ohm steps.
5. 60 capacitors—0 to 1.0 microfarad in 0.01-microfarad steps.
6. 40 capacitors—0 to 5.0 microfarad in 0.01-microfarad steps.
7. 10 capacitors—0 to 100 microfarad in 10-microfarad steps.
8. 25 special transformers.‡

B. Five Plug-boards

1. Plug-boards for all elements of two circuit elements cabinets.
2. Receptacle patch board for 500 element connections and 25 main busses.

C. One Forcing Functions Cabinet

1. Six sinusoidal variable frequency and phase position voltage sources (0 to 50 volts, one ampere, one ohm).
2. Six separate square wave voltage sources (0 to 12 volts, or one 72 volt source in 2-volt steps).
3. Four arbitrary voltage sources (maximum 50 volts, one ohm).
4. Four arbitrary current sources (controllable from any voltage source).

D. One Metering Desk

1. Three dynamometer type meters for metering sinusoidal current and voltage.

* See schematic diagram of Figure 3a.

† See schematic diagram of Figure 3 b.

‡ Additional elements are contemplated. Table lists only elements of computer in its present operating form.

2. Two cathode-ray oscillographs for transient solution (current, voltage, or charge).

3. One 3-wire metering circuit (and selector system) for metering all passive circuit elements and source forcing functions.

4. One manual metering selector circuit for 16 of 25 main busses.

5. Circuits for connecting eight synchronous switches to any of 16 main busses.

E. Amplifier Cabinets (portable)

1. Five negative gain (0 to 50) d-c amplifiers (one ohm impedance), each having an *RC* time delay circuit for servo problems.

2. Ten positive gain (0 to 500) d-c amplifiers with isolated input and one ohm output impedance.‡

F. Multiplier Cabinets (portable)

1. Three multipliers with isolated inputs and one ohm output impedance‡ (five more are being built).

G. Arbitrary Functions of Dependent Variable (portable)

1. Three voltage limiters.
2. Two current limiters.
3. Two perfectly arbitrary function devices‡ (five more to be built).

Appendix II. Suggested List of Engineering Problems Suited to the Electric Analog Computer

Electric Circuits

The computer includes many of the facilities of an a-c or d-c network analyzer. As an a-c analyzer it can handle steady-state problems of importance to electrical utilities. These are briefly listed as follows:

1. Steady-state power flow problems.

2. System short-circuit calculations for relay and circuit-breaker application.

3. System stability problems.

As a d-c calculating board the computer can handle:

4. Distribution circuit analysis.

5. D-c circuit analysis.

The transient facilities will enable it to handle the following important system transient problems.

6. Switching surge and arcing ground studies on power systems.

7. Electronic rectifier and inverter circuit analysis.

8. Transients in other types of circuits.

More general types of circuits than can be analyzed include the following:

9. Aircraft electrical circuit analysis.

10. Ship electrical circuit analysis.

11. Analysis of dielectric and induction heating circuits.

12. Solution of rotating machine equations.

13. Certain special industrial circuit analysis.

14. Plugging steel mill motors; combined electro-mechanical problem.

15. Switching surges in rotating machine windings and surge voltage distribution.

16. Analysis of surge voltage distribution in transformer windings.

17. Dielectric field problems encountered in insulation design.

18. Analysis of automatic load control circuits for turbine generators—also related to servomechanism classification.

The various laboratory facilities will provide a much more ready method than hitherto available for certain types of general analysis:

19. Analysis by frequency-impedance and frequency-reactance curves.

20. Conversion of circuits to other equivalent circuits such as determination of *ABCD* constants.

21. Determination of equivalent circuits from test data.

The multipliers permit the solution of certain important rotating machine equations, certain of which are listed under servomechanisms. The elements for representing arbitrary functions of the dependent variable of course extend the computer's usefulness in the general nonlinear field and enable the solution of such important electrical problems as ferro-resonance, electronic and rotating machine performance under saturated conditions.

Applied Mechanics

One of the largest fields of application for such an analyzer has been found to be in the field of mechanics. Vibration analysis has hitherto been entirely inadequate in most design departments. With the computer many of the analyses can be made in dimensionless form suitable for quite general applications. Examples of such studies that have been made are the following:

22. Electronic tube packaging and chassis shock analysis.

23. Vibration test table analysis.

The following is a partial list of typical lumped constants vibration problems:

24. A-c turbine generator short-circuit torques.

25. D-c generator short-circuit torques.

26. Steel mill plugging torques.

27. Vibratory analysis of ship's drives.

28. Gas and diesel engine vibratory analysis.

29. Problems of motor starting and acceleration through resonance frequencies of mechanical systems.

30. Analysis of certain kinds of timing mechanisms.

31. Determination of damping factors and other parameters by correlation with test data.

32. Analysis of effect of imperfections in large gear teeth.

33. Complete analysis of regulated rotating systems (see servomechanisms).

There are similar types of mechanical systems involving translational motion, such as:

34. Shock problems such as encountered in naval ship equipment when guns are firing.

35. Analysis of mechanical wave filters.

Methods of analysis have been developed for all forms of lumped constants systems undergoing any form of motion, or combined translational and rotational motion. Such problems can readily be handled for all systems as long as the angular motion is relatively small:

36. Rail car truck stability.

37. Radial aircraft engine vibration analysis.

38. Aircraft generator vibration analysis.

39. Vibration testing devices for aircraft.

40. Gyroscope analysis.

41. Analysis of vehicle ridability.

Beam Vibration Problems

This covers a very broad field of important design problems, many too complex for analysis by any other known means. The basic analogies have been developed for the general beam under all forms of support and loading. Multiple beams connected through various forms of lumped constant elements can also be analyzed.

42. Static loading of beams.

43. Dynamic beam loading and vibration.

44. Determination of critical shear stresses and bending moments.

45. Determination of critical frequencies of beams.

46. Straight and curved beams.

47. Multiple beam systems such as shafts with rotating inertias mounted through bearings to frames composed of beams.

48. General combined beam and lumped mechanical constant systems.

49. Air frame flutter problems (combined torsion and bending).

50. General analysis of methods of acceleration damping (air frames).

51. Dynamic and pulse studies of steam turbine blades.

For the latter three items the number of elements available in the initial analyzer installation will probably not be sufficient for some of the more complex problems of importance to industry. However, valuable basic studies can be made on some of the simpler systems. These may permit generalization or show that addition to the number of board elements is justified. This statement also applies to several of the following items:

52. Beam shock problems, such as landing shocks on air frames.

53. Strain analysis of sucker-rod oil well pump systems.

54. Oscillations in suspension bridges.

55. Beams on elastic foundations.

56. Analysis of nonsymmetrical rotors on shafts.

Self-Induced Mechanical Oscillations

57. Airplane structure flutter.

58. Systems with instability induced by oil lubricated bearings, and so forth.

59. Car shimmy, and so forth.

60. Rail car truck stability.

61. Oscillations in closed surge tanks (also hydraulic).

General Theory of Elastic Stability

62. Stability of shear webbs of beams (plate girders).

63. Buckling of columns under end loading.

Thermodynamics

Transient and steady-state heat flow problems are another important field of application for the Computer. Most of these will also be problems involving one or more continuous conducting media. However, it has been found that a great many of such problems can be adequately handled with the number of elements being considered here.

64. General analysis of both steady-state and transient heat flow problems. Of particular general importance would be analysis correlated with test data to determine certain coefficients of heat transmission and surface conduction.

65. General studies of economic thickness of thermal insulation for intermittent operation.

66. Thermal ratings and temperature studies of various machines under intermittent operation.

67. Temperature distribution in electrical coils.

68. Ventilation design problems—rotating and static machine design.

69. Effect of through metal in insulated walls.

70. Temperature gradients in lubricating films and bearing shells.

71. Temperature distribution and heat flow in gas turbines and jet engines.

72. Transient heating of turbines.

73. Transient heating in motor and generator.

74. Damper rings and bars.

There are several other distributed constants media to which the method of electrical analogy and the analyzer can have important application. These include electric current distribution in large electrical conducting media, hydraulic problems, and electromagnetic wave propagation. Hydraulic problems include the following:

75. Penstock problems.

76. Water hammer.

77. Valve stability.

Combined Physical Systems Including Servomechanism

There are several types of important engineering problems involving several interrelated physical systems. Most of these consist of electrical, mechanical, thermal, and hydraulic elements. Numerous among these are process control systems and the most important types come under the category of servomechanisms. Many of these linear systems are well suited to general analysis in dimensionless form:

78. Transient heating of turbines.

79. Transient heating in motor and generator damper rings and bars.

Angle-Position Controllers

80. Gun controllers.

81. Tank turret controllers.

82. Rail car bank controller.

83. Airplane stabilizers—gyro pilots.

84. Guided missiles.

85. Elevator control systems, and many others.

Voltage Regulators

86. Complete generator system analysis.

87. Rotating exciters.

88. Electronic exciters.

89. Rototrol systems.

Speed Controllers

90. Rolling mill equipment.

91. Generator governors.

92. Analysis of complete wind tunnel drives.

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No Discussion

Progress Report on AIEE-ASME Standard Large 3,000-RPM Turbine Generators

S. H. MORTENSEN
FELLOW AIEE

J. B. McCLURE
MEMBER AIEE

C. M. LAFFOON
FELLOW AIEE

THE report of the joint AIEE-ASME committee on proposed preferred standards for large 3,600-rpm 3-phase 60-cycle condensing turbine generators was made available to the AIEE standards committee in October, 1944. It was later presented under the sponsorship of the power generation committee at the 1945 AIEE winter meeting in New York, N. Y.

AIEE Standard 601 covering the joint AIEE-ASME preferred standards for large 3,600-rpm condensing steam turbine generators was approved by council, ASME, February 23, 1945 and by the board of directors, AIEE, February 27, 1945. The companion AIEE Standard 602 covering standard specification data for generators for large 3,600-rpm condensing steam turbines was approved by the board of directors, AIEE, February 27, 1945.

Table I gives the ratings and characteristics of the six units covered by the preferred standards.

Approximately three years' time has elapsed since the standards were officially adopted for trial use, and it is now appropriate that a review be made to check the progress made by the manufacturers in developing and building the units, and the degree of acceptance of the program by the electric utility industry. This progress report is restricted to the scope of the AIEE portion of the joint committee, although the data submitted covers the complete turbine-generator units. The adoption of the standards came at the time industry was in the process of converting from war to a peace-time program. The development of the six new standard turbine-generator units necessitated the making of engineering designs, manufacturing and customer approval drawings, manufacturing tools and assembly equipment. The drafting load involved in producing drawings for the generators only, represented three to four thousand man-days work. The manufacturers were further faced with a shortage of both experienced and inexperienced draftsmen, design and manufacturing engineers, and hampered by a large flood of orders, strikes and other disturbing economic conditions.

Table I. Preferred Standards for Large 3,600-Rpm, 3-Phase, 60-Cycle Condensing Steam Turbine Generator

	Air-Cooled Generator	Hydrogen-Cooled Generator Rated for 0.5 Pound Per Square Inch Gauge Hydrogen Pressure				
Turbine-generator rating, kilowatts	11,500	15,000	20,000	30,000	40,000	60,000
Turbine capability, kilowatts	12,650	16,500	22,000	33,000	44,000	66,000
Generator rating, kilovolt-amperes	13,529	17,647	23,529	35,294	47,058	70,588
Power factor	0.85	0.85	0.85	0.85	0.85	0.85
Short circuit ratio	0.8	0.8	0.8	0.8	0.8	0.8
		Hydrogen-Cooled Generator Operated at 15 Pounds Per Square Inch Gauge Hydrogen Pressure				
Generator capability at 0.85 power factor, kilovolt-ampere			27,058	40,588	54,117	81,176

Table II. Customer Orders Obtained by the General Electric Company for Preferred Standard Ratings up to December 1, 1947

Kilowatt Rating	Number of Units	Total Kilowatt Capacity
11,500	4	46,000
15,000	0	
20,000	6	120,000
30,000	9	270,000
40,000	20	800,000
60,000	21	1,260,000
Total	60	2,496,000*

* The number of units and total kilowatt capacity represents approximately one-third of the General Electric turbine generator business in sizes of 10,000 kw and larger.

There was an increasing need by the utility industry for the purchase and delivery of more and more steam driven turbine-generator units to meet rapidly increasing loads. Both manufacturer and purchaser were faced with the desire on one hand to use the old type units which were already developed and defer the development of the new units, and on the other hand, to carry through both programs simultaneously. All manufacturers recognized that in many other respects this was an ideal time to introduce the new standard line and that any appreciable delay in the development of the new ratings would seriously jeopardize the over-all acceptance of the program, and thus it was decided to develop the new standard ratings as quickly as possible. At present there are four manufacturing companies in the United States which are interested in building these standard units. Three are concerned with all ratings and one is concerned only with the smaller ratings. The following tables

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S. H. MORTENSEN is chief electrical engineer, Allis-Chalmers Manufacturing Company, Milwaukee, Wis., J. B. McCLURE is with the central station engineering department, General Electric Company, Schenectady, N. Y., and C. M. LAFFOON is manager, a-c generator engineering department with Westinghouse Electric Corporation, East Pittsburgh, Pa.

Table III. Customer Orders Obtained by Westinghouse Electric Corporation for Preferred Standard Ratings up to December 1, 1947

Kilowatt Rating	Number of Units	Total Kilowatt Capacity
11,500	1	11,500
15,000	0	
20,000	3	60,000
30,000	23	690,000
40,000	10	400,000
60,000	19	1,140,000
Total	56	2,301,500*

* The total kilowatt capacity represents approximately 48 per cent of the Westinghouse Electric Corporation's turbine generator business in sizes of 10,000 kw and larger.

Table IV. Customer Orders Obtained by Allis-Chalmers Company for Preferred Standard Ratings up to December 1, 1947

Kilowatt Rating	Number of Units	Total Kilowatt Capacity
11,500	2	23,000
15,000		
20,000	5	100,000
30,000	2	60,000
40,000	7	280,000
60,000	0	
Total	16	463,000*

* This represents approximately 40 to 50 per cent of Allis-Chalmers turbine generator business under construction in sizes of 10 kw and above.

In terms of machines in the same size range, the percentage of standardized units is somewhat higher, and has been steadily increasing. During the second half of 1947 about 75 per cent of the units sold within this band were of the standardized design.

Table V. Customer Orders Obtained by the Elliott Company for Preferred Standard Ratings up to December 1, 1947

Kilowatt Rating	Number of Units	Total Kilowatt Capacity
11,500	1	11,500
15,000	0	
20,000	0	
30,000	0	
40,000	0	
60,000	0	
Total	1	11,500*

* The total kilowatt capacity represents approximately 10 per cent of the Elliott turbine generator business in sizes of 10,000 kw and larger.

Table VI. Customer Orders Obtained by All Manufacturing Companies for Preferred Standard Ratings up to December 1, 1947

Kilowatt Rating	Elliott Company, Kilowatts	Allis-Chalmers Manufacturing Company, Kilowatts	Westinghouse Electric Corporation, Kilowatts	General Electric Company, Kilowatts	Total Kilowatts
11,500	11,500	23,000	11,500	46,000	92,000
15,000					
20,000		100,000	60,000	120,000	280,000
30,000		60,000	690,000	270,000	1,020,000
40,000		280,000	400,000	800,000	1,480,000
60,000			1,140,000	1,260,000	2,400,000
Total	11,500	463,000	2,301,500	2,496,000	5,272,000

show the progress made by the different manufacturing companies and the purchaser acceptance of the new standard line.

Several of the new line units have been built and shipped and are now either in operation or being erected. On the basis of the data and the general situation associated with the production of the new standard line, the following observations and conclusions can be made.

1. All of the standard line units are now available with the exception of the 15,000 kw rating.

2. The purchaser acceptance has been wide, and the number of units and total kilovolt-amperes now on order represent 35 to 40 per cent of the total turbine generator business for units of 10,000 kw rating and above.

3. In 1948 and thereafter, it is anticipated that orders placed for new units will be almost exclusively of the new standard line for the range of ratings covered by this line. It is expected that this will represent approximately 50 per cent of the current turbine-generator business in that period.

4. With the development of the standard line practically complete, the manufacturer can furnish outline and customer approval drawings at the time the negotiation proposals are presented. After the purchase order is placed, both the manufacturer and the purchaser can place orders immediately for manufacturing and construction materials. The purchaser is thus definitely assured of a better product, at reduced cost, and with shorter deliveries.

From comments and suggestions received from both purchaser and manufacturer groups, it is recommended that the existing AIEE committee again become active and consider the following items:

1. Reconsider the 15,000 kw unit and decide whether or not this rating should be designed for hydrogen cooling and have the same characteristics as the four larger ratings.

2. Review the standard specification data for generators for large 3,600-rpm condensing steam turbines for major changes, modifications, and additions.

3. Consider the advisability of standardizing the next larger 3,600-rpm hydrogen cooled unit which is being purchased in appreciable numbers for ratings of 80,000 and 90,000 kw and appropriate power factor and short circuit ratio values.

Discussion

B. H. Smith (Electric Machinery Manufacturing Company, Minneapolis, Minn.): The report mentions calling the committee to discuss suggested changes in the published standards. I would like to ask what changes have been suggested in the generator characteristics.

Frank V. Smith (Sargent and Lundy, Chicago, Ill.): The authors have done a service to the industry by assembling this material on the progress of standardization in large turbine generators. The data show that the standards have been very well received and their greater use should result in improvement in simplification of drawing procedure, economic advantages and improvement in deliveries.

Some comments might be made on the last paragraph of the paper relating to the present situation, in connection with these standards.

Both the AIEE and ASME groups of the joint committee have become active during the last few months, in order to review the points outlined by the authors and also additional items. The status at the present time is the following:

1. With regard to the 15,000 kw unit, the joint committee favors a change from air cooling to hydrogen cooling. It was guided in this decision by the advantages in reduction in losses, increased cleanliness, expected longer life and greater reactive kilovolt-ampere capacity with higher hydrogen pressures.

2. Consideration is being given to a corresponding change in the case of 11,500 kw unit. The situation here, however, is somewhat different in that an appreciable number of standard air-cooled machines have been built. Further, this rating of machine is used to a greater extent in industrial applications, rather than central station applications where the personnel is not as familiar with the problems of hydrogen equipment. At the same time, the same advantages of hydrogen cooling would be obtained as with the larger machines, and there is the feeling that the operators would

find no great difficulty in becoming familiar with the equipment. Inquiries are being made to sections of the industry to determine the sentiment on this point and discussions from members of the Institute would be welcome.

3. Serious study is being made on the possibility of a standard machine larger than 60,000 kw but this work is as yet in the preliminary stages.

The committee would be very glad indeed to have comments from operating engineers on all phases of turbine generator standardization both with regard to the proposed extension of the standards, and also with regard to minor or major modifications that might be considered worthwhile in the standards as they exist.

Balanced Fractional-Slot Wave Windings

M. M. LIWSCHITZ
MEMBER AIEE

THE a-c wave windings which are used most frequently in wound rotors of induction motors are "modified" d-c wave windings whose winding pitch is changed after each p* winding elements. These modified wave windings are balanced, but they can be applied to integral-slot and (integral+1/2)-slot windings only. The standard d-c wave winding is not used for wound rotors of induction motors for it is seldom balanced. The aim of this paper is to give a method of laying out balanced wave windings for any fractional number of slots per pole per phase for which the fractional-slot lap winding is balanced. For this purpose, the usual chart method of investigating fractional slot windings is to be abandoned and the slot star of the winding used instead.[1]

The Slot Star and the Winding Pitch

If the number of slots per pole per phase is written in the form

$$q=\frac{N}{\beta} \tag{1}$$

where N and β have no common divisor, β is the number of poles in a repeatable group of the winding, and N the number of slots per phase per repeatable group. $2p/\beta$ is the number of repeatable groups.

The slot star shows the position of the slots in the magnetic field. There are as many vectors in the slot star as there are slots in a repeatable group, that is, mN, and there are as many coinciding slot stars as there are repeatable groups. Figure 1 shows the slot star of a 3-phase winding with $q=1-4/5$. For this winding, $N=9$, and the number of vectors in the slot star is $3\times 9=27$. Two adjacent slots are not represented in the slot star by two adjacent vectors, but between two adjacent slots lie $(\beta-1)$ other vectors. (In Figure 1, $5-1=4$.) This is due to the fact that the fractional-slot winding creeps in the magnetic field and the angle between two adjacent vectors is, therefore, smaller than that between two adjacent slots. The difference between two slots which correspond to two adjacent vectors is

$$d=\frac{mNP+1}{\beta} \tag{2}$$

where P is the smallest integer which makes d an integer. For the winding of Figure 1, where $m=3$, $N=9$, and $\beta=5$,

$$d=\frac{3\times 9\times P+1}{5}=11 \text{ where } P=2$$

Corresponding to this value of d, the vectors follow the series

$$1,\ 1+11=12,\ 12+11=23,$$
$$23+11=34-3\times N=7,\ 7+11=18\ldots.$$

Because the slot star contains $mN=27$ slots, this quantity is to be subtracted when a term of the series becomes larger than 27.

The maximum distribution factor of the main wave is obtained for the winding, if the slots assigned to each phase lie as close together as possible, for example, if in Figure 1, the first $N=9$ slots are assigned to phase A, the following nine slots to phase C, and the last nine slots to phase B. This is the usual way to assign the slots to the three phases in fractional-slot lap windings.

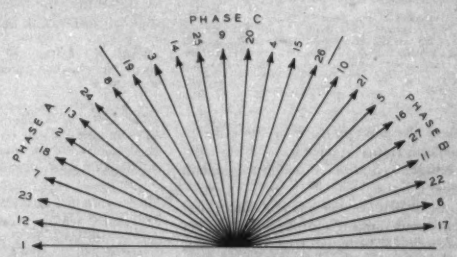

Figure 1. Slot star of a 3-phase winding with $q=1-4/5$

In order to determine the slots which are to be assigned in this manner to the three phases, it is not necessary to draw the slot star. The d-series does this in a simpler way. This series yields, for example, for phase A of the winding considered in Figure 1 the slots,

1 12 23 7 18 2 13 24 8

This is in accordance with the slot star. Arranging the nine slots in the sequence in which they follow in the machine, that is,

1–2 7–8 12–13 18 23–24

four groups with two slots each and one group with one slot are obtained.

With two conductors per slot, one in each layer, the slot star shows the position in the magnetic field of not only the slots,

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M. M. Liwschitz is a professor with the Polytechnic Institute of Brooklyn, Brooklyn, N. Y., and also is connected with the Westinghouse Electric Corporation, East Pittsburgh, Pa., as consulting engineer.

*See List of Symbols.

but of the upper or lower conductors as well.

Making, at first, the assumption that there are only two conductors per slot, the nine figures obtained from the d-series for phase A represent the nine upper conductors assigned in each repeatable group to this phase. In the lap winding two consecutive upper conductors lie in adjacent slots, for example, in slots 1 and 2, 7 and 8, and so forth. In order to obtain maximum distribution for the main wave in the wave winding, the same conductors must be assigned to the phases as in the lap winding, yet in the wave winding two consecutive upper conductors must lie not in adjacent slots, but y slot pitches apart. The upper or the lower conductors of the wave winding must follow the y-series.

$$1,\ 1+y,\ 1+2y,\ 1+3y \ldots\ldots$$

The problem of laying out a balanced wave winding consists in the co-ordinating of the d-series and y-series of the winding.

Layout of A Balanced Wave Winding

Because the number of slots in two poles, $(2mN)/\beta$ is a fractional number, the winding pitch must be

$$y=\frac{2mN+a}{\beta} \tag{3}$$

where a is the smallest integer which makes y an integer (a can be positive as well as negative).

The magnitude of a determines the kind of co-ordination of the d-series and y-series and through it the number of winding parts in each phase. The sign of a determines whether the winding goes in the same direction as the numbering of the slots or opposite to it

Due to the fact that each p coils of the wave winding go around the whole rotor or stator, the consideration of the wave winding cannot be limited to one repeatable group, but must comprise all $2p/\beta$ repeatable groups. This means that in the case of the wave winding, all $2p/\beta$ slot stars or, what is the same, all $2p/\beta$ d-series must be considered simultaneously. If the slots of the machine are numbered by consecutive digits, the slot star of the second repeatable group is obtained from the slot star of the first repeatable group by adding the quantity mN to all numbers of the latter slot star. The same applies to the d-series. When the first d-series is set up, the second d-series is obtained by adding mN to the numbers of the first d-series, the third by adding mN to the numbers of the second d-series, and so forth. The $2p/\beta$ d-series represent the upper or lower conductors of the whole winding.

The vectors of the $2p/\beta$ slot stars coincide, that is, if the d-series are written one above the other, the $2p/\beta$ conductors which lie in the same column are in phase and can be substituted one for the other. This fact makes it possible to lay out balanced wave windings.

The method of laying out a balanced wave winding and the influence of the magnitude and sign of a (equation 3) will be demonstrated by several examples:

(*a*) $a=+1$.

It follows from equations 2 and 3 that in this case $y=d$, that is, the y-series and d-series are identical. Therefore, the winding can be made up by using the consecutive vectors of the slot-star or their in-phase vectors or, what is the same, by using the consecutive numbers of the $2p/\beta$ d-series. The 3-phase winding shown in Figure 1 is an example for $a=+1$. For this winding, d has been determined to be 11. From equation 3

$$y=\frac{6\times 9+a}{5}=11=d \text{ with } a=+1$$

Assuming that the machine has ten poles, there are $10/5=2$ d-series. They are for phase A

$$\left.\begin{array}{ccccccccc} 1 & 12 & 23 & 7 & 18 & 2 & 13 & 24 & 8 \\ 28 & 39 & 50 & 34 & 45 & 29 & 40 & 51 & 35 \end{array}\right\} d\text{-series}$$

Hence, the two y-series, that is, the 2 winding parts of phase A are

$$\left.\begin{array}{ccccccccc} 1 & 12 & 23 & 34 & 45 & 2 & 13 & 24 & 35 \\ 28 & 39 & 50 & 7 & 18 & 29 & 40 & 51 & 8 \end{array}\right\} y\text{-series}$$

The winding goes clockwise, with the numbering of the slots.

(*b*) $a=-1$.

In this case, $y=mN-d$ and the y-series will be found in the slot star or d-series running opposite to the numbering of the slots. Consider a 10-pole 3-phase winding with $q=2^1/_6$. For this winding $N=11$, $\beta=5$, and

$$d=\frac{33\times P+1}{5}=20$$

$$y=\frac{6\times 11+a}{5}=13 \text{ with } a=-1$$

$$y=mN-d=33-20=13$$

The d-series of this winding for phase A are

1	21	8	28	15	2	22	9	29	16	3
34	54	41	61	48	35	55	42	62	49	36

Considering these series, the y-series is found going from the right to the left. For example, in the first d-series

$$3,\ 3+13=16,\ 16+13=29,$$
$$29+13=42-mN=9\ldots.$$

This winding goes counter clockwise. The two y-series, that is, the two winding parts of phase A are

1	54	41	28	15	2	55	42	29	16	3
34	21	8	61	48	35	22	9	62	49	36

(*c*) $a=+2$.

In this case, $y=2d$ and the y-series is represented by the alternate numbers of the d-series. This leads to twice as many parts in each phase as when $a=+1$. The winding goes clockwise for a is positive. Consider a 10-pole 3-phase winding with $q=3^3/_5$. For this winding, $N=18$, $\beta=5$, and

$$d=\frac{54\times P+1}{5}=11$$

$$y=\frac{6\times 18+a}{5}=22 \text{ with } a=+2$$

$$y=2d=22$$

The d-series of this winding for phase A are

1	12	23	34	45	2	13	24	35	46	3	14	25	36	47	4	15	26	d-series
55	66	77	88	99	56	67	78	89	100	57	68	79	90	101	58	69	80	

The four y-series, that is, the four winding parts of phase A are

1	23	45	67	89	3	25	47	69
12	34	56	78	100	14	36	58	80
55	77	99	13	35	57	79	101	15
66	88	2	24	46	68	90	4	26

The first and second winding parts are not in phase and must be connected in series. The same applies to the third and fourth winding parts. When N is odd, the first and third winding parts have one coil more than the second and fourth winding parts.

(*d*) $a=-2$ *and* $a>2$.

In the case $a=-2$, $y=2(mN-d)$ and the y-series is represented, as in the case $a=+2$, by the alternate numbers of the d-series. However, the y-series runs opposite to the d-series similar to the case $a=-1$, and the winding goes counter clockwise. When $a>2$ the number of parts of each phase is $(2p/\beta)\times a$. Again, the winding goes clockwise, when a is positive, and counter clockwise, when a is negative.

The assumption has been made that there are only two conductors per slot. If the number of conductors per slot is larger than two, the winding can be considered as one having a number of slots equal to the actual number of slots times the number of conductors per slot per layer. This makes each slot have only two conductors and the rules for laying out the wave winding previously given can be applied.

The fractional slot wave windings explained before are balanced and have the same magnetomotive force curves as the corresponding fractional slot lap windings, because the same slots are assigned to the three phases in both windings. Also, the maximum possible number of parallel paths

is the same as for the fractional slot lap windings. Comparing the wave windings explained here with the "modified" d-c windings, the latter have only two winding parts per phase, independently of the number of poles. The number of winding parts per phase of the windings considered here depends upon the number of slots and the magnitude of a. On the other hand, the modified d-c windings have abnormal front pitches after each p coils, while in the windings just described, all coils are equal. The manufacturing of the latter windings is, therefore, less expensive than that of the modified d-c windings, except in the cases when a is large.

The designer who uses the integral slot or (integer + 1/2) slot wave winding may obtain a less expensive winding by using the layout methods previously described.

List of Symbols

a = smallest integer which makes y an integer
d = difference between two slots which correspond to two adjacent vectors of the slot star
m = number of phases
N = numerator of fraction which fixes the number of slots per pole per phase
p = number of pole pairs
P = smallest integer which makes d an integer
$q = N/\beta$ number of slots per pole per phase
y = winding pitch (back pitch + front pitch)
β = denominator of the fraction which fixes the number of slots per pole per phase

Reference

1. Distribution Factors and Pitch Factors of Harmonics of a Fractional Slot Winding, M. M. Liwschitz. AIEE Transactions, volume 62, 1943, pages 664–6.

No Discussion

Operation of Large D-C Motors From Controlled Rectifiers

A. SCHMIDT, JR.
MEMBER AIEE

W. P. SMITH
Membership Application Pending

Synopsis: The d-c output voltage of a typical power rectifier has more ripple than the output of a d-c generator. The ripple increases as the number of rectifier phases is decreased. It also increases as the rectifier voltage is reduced by phase retard. The voltage ripple affects the performance of d-c motors in the following respects:

1. Heating.
2. Commutation.
3. Speed regulation, particularly when current is discontinuous.

D-c motors constitute the major part of the load of general purpose power rectifiers. It is a matter of experience that d-c motors in the fractional horsepower and small integral horsepower ranges must be derated when they are operated over a wide speed range from rectifiers with voltage control. The factors which result in derating of these motors are present to a very limited extent in general purpose rectifiers, which usually have six or more phases and are seldom operated with more than 15 per cent phase control. Hence little consideration has been given to the derating of motors connected to such rectifiers. In recent times the extension of controlled rectifier drives to d-c motors in the range of 100 horsepower or more has raised the question of modifying the rating of these larger motors when operated from rectifiers with phase control. It is the purpose of this paper to discuss the factors present in controlled rectifiers which affect the rating of connected motors and to describe the results of tests made on a 400-horsepower motor driven by both a 6-phase and a 3-phase controlled ignitron rectifier. Emphasis in this paper will be placed on the practical significance of the test results obtained.

Effects of Rectifier Supply on Motor

Voltage and Current Ripple

Case (1) Continuous Current. Conventional methods of rectifier circuit analysis assume an infinite d-c reactance. This assumption results in steady d-c current and facilitates circuit analysis. Since we are concerned here with the effect of a current which is not steady, but pulsates at some multiple of the supply frequency, we must consider the value of circuit inductance since this is a factor in determining the ripple.

In the case of continuous (that is, nonintermittent) current, the d-c inductance is made up of two parts, (1) the inductance of the load, and (2) the average inductance of the conducting windings of the rectifier transformer.

With usual ratios of resistance to inductance, resistance has a negligible effect on ripple.

Figure 1 shows the voltage and current of a typical 6-phase rectifier having a counter electromotive force load with finite inductance. The voltage is made up of a d-c component and an a-c component whose frequency depends on the number of phases in the rectifier and the supply frequency. The average value, rms value, and harmonic content of the a-c component may be determined by established methods.[1]

At a moment when the voltage of the rectifier is at its average value and rising, the motor current will be at its minimum value. If this time be designated by t_0 and the next instant of equal rectifier voltage by t_1, the motor current will vary over the interval by an amount

$$\Delta I = \frac{\int_{t_0}^{t_1}(E_d - e_d)dt}{L_d}$$

$\int_{t_0}^{t_1}(E_d - e_d)dt$ is proportional to the average value of the rectifier ripple voltage. Thus, the current ripple is a function of average voltage ripple of the rectifier and the d-c inductance. The average ripple voltage of the rectifier depends on the number of phases, the angle of phase retard, the rectifier load, and the transformer reactance. Figure 2 shows the average ripple voltage of rectifiers as a function of phase retard angle and number of phases for a typical value of transformer reactance. The total current variation with q phases is

$$\Delta I = \frac{E_{av}}{2qfL_d}$$

Thus, the current ripple can be determined if the number of rectifier phases, the amount of phase retard, and the total d-c circuit inductance are known.

Case (2) Discontinuous Current. Conditions are sometimes present where the half-amplitude of the ripple current exceeds the average value of current in the motor, in which case the current will not be continuous. Figure 3 illustrates the voltage and current relations in such a case. If conduction is discontinuous, a given anode cannot start to conduct until its voltage exceeds the counter electromotive force of the motor plus the arc drop voltage. Conduction may be delayed to a later time by phase control of the rectifier. In Figure 3 conduction is assumed to start at some time after the anode voltage zero represented by the angle ψ. During the period when the anode voltage exceeds the motor voltage, current will increase to some maximum value which is reached when the anode voltage falls back to the value of the motor voltage. At this point, the stored energy in the inductance will maintain current for some period. If resistance is neglected, the current will reach zero at such a time that the integrated volt-seconds difference between the anode voltage and counter electromotive force is the same for both sections of the conducting period. In other words, areas A and B of Figure 3 are equal.

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A. Schmidt, Jr. and W. P. Smith are both with the General Electric Company, Schenectady, N. Y.

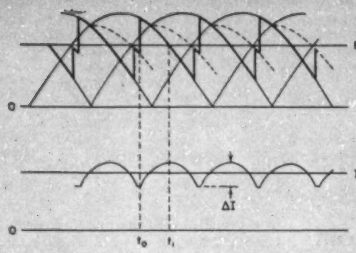


Figure 1 (left). Direct voltage and current of 6-phase rectifier with moderate value of load inductance

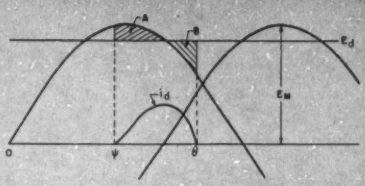


Figure 3. Voltage and current relations in controlled rectifier with noninductive counter electromotive force load

Regulation

Figure 4 shows the regulation curve of rectifiers with various amounts of phase retard and carrying discontinuous current. The derivation of these curves is shown in the Appendix. Zero resistance is assumed. The arc drop of the rectifier is treated as part of the total counter electromotive force.

It will be noted that if the conducting period of one anode exceeds $360°/q$ then conduction of current to the load is continuous and not discontinuous. In such a case the regulation of the rectifier approximates the figure derived when rectifier circuit theory is applied to the case with infinite d-c inductance. When conduction is discontinuous, it is immaterial whether the inductance is confined to the d-c circuit or the a-c circuit or is distributed between the circuits. Figure 4 indicates the end of discontinuity for rectifiers having different numbers of phases.

Temperature Rise

A motor operating on rectified power may be expected to show a greater than normal temperature rise because of the greater losses resulting from the effects of ripple current. These added losses are divided between the current carrying and flux carrying parts of the machine. The former are increased by the extra eddy current losses, by the increase in effective resistance caused by skin effect, and by reason of the fact that the ratio of rms to average current is greater than unity. The flux carrying material in the motor will show increased eddy current and hysteresis loss when ripple current is present because of resulting flux pulsations. Such losses will be accentuated in motors having series fields. The magnitude of all these extra losses is a function of the magnitude and frequency of the ripple current and thus is influenced by the degree of phase retard, increasing as the angle of retard is increased.

Commutation

The commutating performance of a d-c motor operating from a rectifier power supply is adversely affected when the frequency of the ripple current is so high that the commutating pole flux is no longer proportional to and in phase with armature current. When this occurs the armature reaction is not properly compensated for and sparking may result. Nonproportionality may be caused by reduction of commutating pole flux by eddy currents, phase displacement due to iron loss, or by saturation of the pole at current maxima.

Motor Tests With Rectifier Supply

In order to evaluate the effect of a polyphase rectifier supply on a medium or large sized motor, tests were made on a 400-horsepower, 375/750 rpm, 230-volts, direct current, stabilized shunt wound, force ventilated motor of a type commonly used for steel mill and electric shovel applications. Tests were made using both a

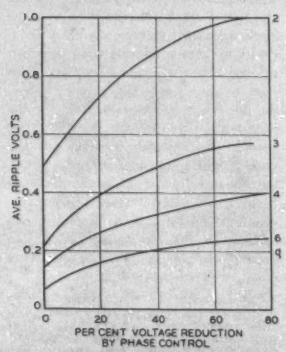


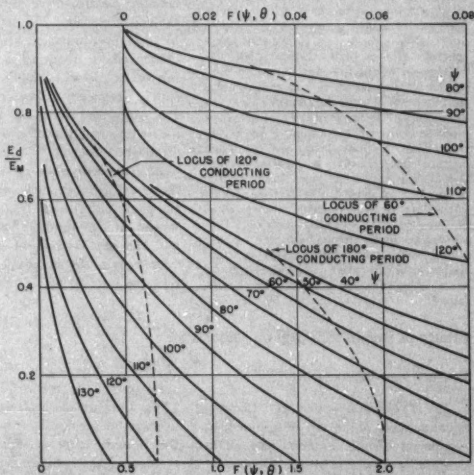
Figure 2. Average ripple volts in output of typical rectifiers at rated load

Per unit base is full load voltage of rectifier with no phase retard

Figure 4. Regulation of rectifier supplying discontinuous currents to zero resistance counter electromotive force load

$$I_d = \frac{qE_M}{2\pi X} F(\psi\theta)$$

See Appendix I for explanation of symbols



6-phase and 3-phase power supply. To permit direct comparisons with normal performance, duplicate tests were made concurrently using a d-c generator as a source of direct current. The test work performed was intended to indicate what effect the rectifier supply would have on the temperature rise, commutation, and speed regulation of a large motor, and in addition a number of oscillograms of current, voltage, and so forth were made in order to evaluate the magnitude of the ripple currents present. The motor was equipped with two exploring coils, one on the tip of one of the main poles and one on the tip of one of the commutating poles. The purpose of these coils was to determine the magnitude of the pulsating component of flux in the poles, and to determine the phase relationship between commutating pole flux and armature current.

Temperature Rise

The extent to which the added losses alter the observed temperature rise was determined by making comparative heat runs using a conventional d-c source, a 6-phase rectifier and a 3-phase rectifier. Comparisons were made on the basis of the same average current since this results in the same torque. In every instance the motor was run under constant load, with normal forced ventilation, until the temperature ceased to rise; whereupon the machine was shut down and the temperature rise of all parts measured by means of thermocouples and mercury thermometers. In addition the average temperature rise of the armature winding was obtained by measuring the hot resistance at the completion of each heat run, and comparing this with the resistance at a known temperature.

The results of the heat runs are tabulated in Table I. Temperature rises are given in per-unit, the values obtained with conventional d-c power being expressed as unity.

Table I. Comparison of Motor Temperature Rises With Generator and Rectifier Supplies

Location of Rise	Temperature Rise. Per Unit Base is Rise With Generator Supply					
	Full Voltage			25 Per Cent Voltage		
	Generator Supply	Rectifier Supply 6-Phase	Rectifier Supply 3-Phase	Generator Supply	Rectifier Supply 6-Phase	Rectifier Supply 3-Phase
Average of rises measured on armature	1.000	0.939*	1.071	1.000	1.061	1.367
Average of rises measured on frame and field structure	1.000	0.986*	1.200	1.000	1.157	1.666
Average of rises measured on field coils	1.000	1.015	1.147	1.000	1.079	1.523
Average of rises measured on pole tips	1.000	0.973*	1.188	1.000	1.306	2.080
Armature rise by resistance measurement	1.000	1.037	0.824*	1.000	1.207	2.243*

*Inaccuracies of measurement believed excessive in terms of per unit base.

Commutation

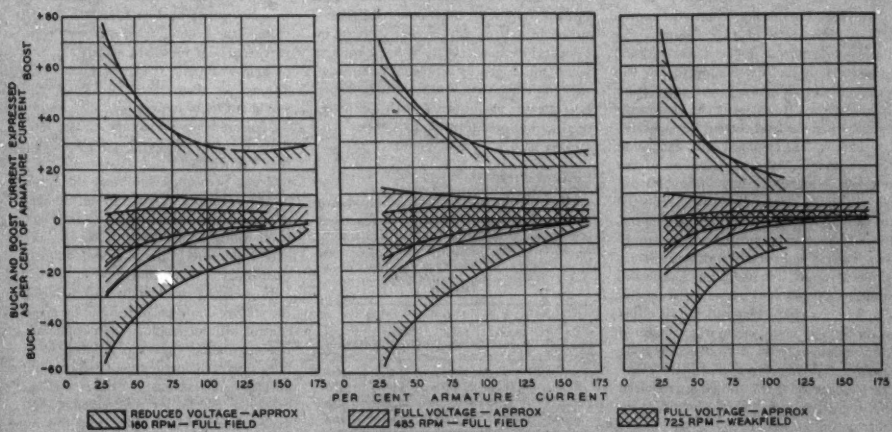
Commutating performance was evaluated by means of "black band" tests, which are familiar to most d-c machine designers. In this type of test the strength of the commutating field is varied above and below normal by means of a booster generator connected across the commutating field winding and the maximum and minimum field strengths at which sparkless commutation exists are determined. Such readings are usually taken at a number of loads and plotted on a chart with load amperes as the abscissae and amperes added to or subtracted from the commutating pole winding as positive and negative ordinates respectively. When the points of maximum commutating field current for sparkless commutation are connected by one line and the points of minimum commutating field current for sparkless commutation by another line, the area between represents a region of sparkless or "black" commutation and hence is commonly known as the "black band."

It will be appreciated at once that the width of this band is an indication of the ability of the machine to operate satisfactorily when commutating pole flux and

Figure 5. Black band commutation curves for 230-volt 400-hp 375/750 rpm motor operating with generator supply

Figure 6. Black band commutation curves for 230-volt 400-horsepower 375/750 rpm motor operating with 6-phase controlled rectifier supply

Figure 7. Black band commutation curves for 230-volt 400-horsepower 375/750 rpm motor operating with 3-phase controlled rectifier supply



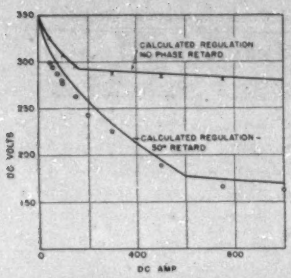


Figure 8. Observed regulation of 3-phase rectifier with 400-horsepower 230-volt motor

armature current are not matched as to magnitude and phase relationship.

In this investigation tests were made under three general conditions of operation as follows:

1. Reduced voltage, full field excitation.
2. Full voltage, full field excitation.
3. Full voltage, reduced field excitation.

Each of these conditions was investigated with conventional d-c supply, 6-phase rectifier supply and 3-phase rectifier supply, with the results shown in Figures 5, 6, and 7. The ripple component of current was prevented from flowing around the commutating field and through the booster generator by inserting a reactor in one of the booster generator leads.

Measurements were made of exploring coil voltages under various conditions of operation.

Speed Regulation

The speed regulation of any d-c motor is a combination of two factors; the inherent regulation of the motor and the regulation of its power supply. In the case of a rectifier power supply regulation is only a few per cent over the load range except when the load current becomes discontinuous. As pointed out earlier in this paper the voltage regulation of the rectifier is relatively large when discontinuous currents exist and this is reflected in the speed regulation of the motor. Since discontinuous currents occur mainly at relatively light loads and large values of phase retard, it is under low torque and speed conditions that motor speed regulation is increased. This will not be a disadvantage in many cases where lowered voltages occur only during accelerating periods, but in other applications where creeping speeds at low torque are required this increased speed regulation may require consideration.

In order to obtain factual data regarding this effect voltage regulation curves were made with various values of phase retard. The results have been plotted and compared to calculated performance in Figures 8 and 9.

Ripple Current

In order to evaluate the magnitude and wave shape of the d-c currents and voltages, oscillograms were taken showing load voltage, armature voltage, armature current, and anode current for one tube under various conditions of load and phase retard. Representative results are shown in Figures 10 and 11.

Calculated values of ripple current were derived from the angle of phase retard of the rectifier and the measured motor inductance. Typical values are given in Table II.

Discussion of Test Results

Temperature Rise

The heat run results shown in Table I show that in the motor under test, the field structure temperature rise is increased by a larger percentage than is the rise of the armature. However, the normal temperature rise of the field is generally less than that of the armature so that in most cases the armature is still the hottest part. This particular motor, as noted before, was run with forced ventilation, probably resulting in less increase in rise at reduced voltage than would have been experienced with a self-ventilated motor. Examination of the data given in Table I shows that at full voltage the 6-phase rectifier power supply does not appreciably affect the temperature rise but the 3-phase rectified power supply resulted in a 10 to 20 per cent increase in temperature rise. At 25 per cent voltage where much greater ripple currents exist the temperature rise was from 10 to 20 per cent greater than normal with a 6-phase rectifier but was from 35 to 120 per cent greater with a 3-phase rectifier.

Commutation

Comparison of the black bands shown in Figures 5 and 6 will show clearly that the 6-phase rectifier has practically no

Table II

Number of Phases	Degree Phase Retard	Load Ampere	ΔI, Calculated	ΔI, Observed
3	0	1,500	435	480
3	27	1,500	640	700
3	63	1,500	1,050	1,120
6	66	1,600	230	220
6	26	1,500	120	130

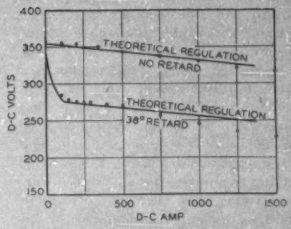


Figure 9. Observed regulation of 6-phase double wye rectifier with 400-horsepower 230-volt motor

adverse effect on the commutating characteristics of this motor even at reduced voltages. Some narrowing of the band at reduced voltage is discernible but since the band is so wide sparkless commutation is obtained over a wide range of load. This widening of the band at reduced voltages is inherent in all d-c motors and is a most fortunate circumstance since it means that any motor is thus better able to handle the increased ripple currents which exist when the rectifier voltage is lowered by retarding the firing.

A comparison between performance with a generator supply and with a 3-phase rectifier as indicated by reference to Figures 5 and 7, shows a definite inferiority of performance with 3-phase rectified power, although in this case the observed commutation was still sparkless over most of the load range.

In an effort to determine the extreme limits of commutating ability the motor was supplied with 3-phase rectified power at 200 volts from a 600-volt phase-retarded rectifier. With the ripple current thus intensified at approximately normal voltage the commutation was very poor, particularly at increased speeds obtained by field weakening. However, such conditions are considered abnormal and would not occur in practice.

Measurements of voltage on the commutating pole exploring coils indicated that the ripple in the commutating flux was less percentagewise than the ripple in the armature current by an amount approximating the narrowing of the black band width. This confirms the presence of eddy currents in the poles and indicates their order of magnitude.

In general it is thought that reducing the voltage of a 6-phase rectifier by phase control will not result in poor commutation, assuming that the rectifier when fully advanced does not deliver more than normal motor voltage. This is true because the margin for good commutation varies inversely with speed and this in-

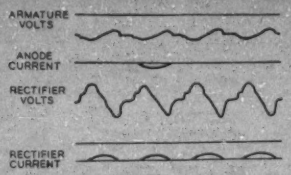


Figure 10. Current and voltage relations in 400-horsepower 230-volt motor driven by 300-volt 3-phase rectifier at 215-volts 150-amperes

creased margin at low speeds more than compensates for the detrimental effects of the increased ripple component in the current.

Regulation

The observed voltage regulation of the rectifier shown in Figures 8 and 9 agree with performance calculated from the inductance of the motor and the rectifier.

It will be noted that the subject motor is a medium speed machine. A higher speed motor, or one having compensating poles, may have considerably less inductance. This will result in discontinuity of current at higher values of load, requiring a greater range of rectifier phase control if it is desired to maintain constant motor speed over a considerable load range.

Conclusions

Motor Inductance

It should be borne in mind that the unique result of application of a rectifier as a power source to a motor is ripple current and that the magnitude of this ripple current varies inversely with the motor inductance. The results of tests on a particular machine must be interpreted with this in mind.

Temperature Rise

It appears that no derating due to increased temperature rise is indicated for operation on 6-phase rectifiers at full voltage. Some slight derating might be required if extended operation at low voltage were contemplated, but it is probable that most duty cycles would not include enough low voltage operation to necessitate derating the machine.

When operating from a 3-phase rectifier delivering full voltage, a certain amount of derating would be necessary in order to maintain rated temperature rise. When operating at 25 per cent voltage on the 3-phase rectifier considerable derating would be necessary to prevent overheating of the motor. However, the use of 3-phase variable voltage rectifiers with motors larger than 75 horsepower is not generally contemplated.

The fact that the greatest increase in temperature rise in every case was in the frame and field structure of the machine would seem to indicate that considerably increased iron loss is present and it may be necessary in some cases to design the stationary magnetic flux carrying structure for lower losses. Since heat in the field structure influences the field coil temperature, the design of field coils may require review.

Commutation

No discernible lessening of commutating ability of the subject machine appears to result when a 6-phase rectifier is used. Even at reduced voltage the machine performs practically as well as with a generator supply. With a 3-phase rectifier, the width of the "black bands" was reduced, and machines having very narrow black bands on a conventional d-c supply may show distress when operated with 3-phase rectified power.

Regulation

Reduced voltage operation of the rectifier will result in poor speed regulation of connected motors at light loads, particularly in the case of low-inductance machines, unless a suitable voltage regulator is applied to the rectifier.

Appendix I. List of Symbols

e_d = instantaneous d-c volts
E_d = average d-c volts or motor counter electromotive force
E_{av} = average value of ripple volts
E_M = peak value of rectifier d-c volts with no phase retard
I_d = average value of rectified current
L_d = d-c circuit inductance
q = number of rectifier phases
X = total series reactance of rectifier circuit

Appendix II

Figure 3 shows the voltage and current relations for a rectifier consisting of a single rectifying element supplied by an a-c voltage having a crest value E_M, with a constant potential load E_d and a circuit inductance L. Current flow cannot start before the anode voltage reaches the value E_d. It may be delayed by grid control to a later time.

Let ψ be the angle at which conduction starts. Current will increase till the time γ, where the anode voltage is equal to E_d. Current will then decrease and reach zero at time θ, when the inductance will have returned to the circuit the energy stored in it during the interval from ψ to γ. If no circuit losses exist, areas A and B are equal.

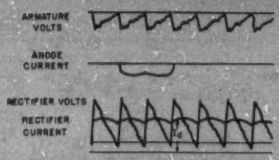


Figure 11. Current and voltage relations in 400-horsepower 230-volt motor driven by 300-volt 6-phase (double wye) rectifier at 93-volts 1,500-amperes

$$A = E_M \int_\psi^\gamma \sin \omega t \, dt - E_d(\gamma - \psi)$$

$$= B = E_d(\theta - \gamma) - E_M \int_\gamma^\theta \sin \omega t \, dt$$

whence

$$\frac{E_M}{\omega}(\cos \psi - \cos \theta) = E_d(\theta - \psi)$$

Since

$$E_d = E_M \sin \gamma$$

$$\sin \gamma = \frac{\cos \psi - \cos \theta}{\omega(\theta - \psi)}$$

The current i_d is derived from the equation

$$L\frac{di_d}{dt} + E_d = E_M \sin \omega t$$

The solution of this equation is

$$i_d = \frac{E_M}{\omega L}(\cos \psi - \cos \omega t) - \frac{E_M}{L} \sin \gamma(t - \psi)$$

The average current is

$$I_d = \frac{1}{2\pi}\int_\psi^\theta i_d \, dt$$

$$= \frac{E_M}{2\pi X}[\sin \psi - \sin \theta + {}^1/_2(\theta - \psi)(\cos \psi + \cos \theta)]$$

$$= \frac{E_M}{2\pi X}F(\psi, \theta)$$

where

$$F(\psi, \theta) = \sin \psi - \sin \theta + {}^1/_2(\theta - \psi)(\cos \psi + \cos \theta)$$

If the rectifier has q anodes, the total output is

$$I_d = \frac{qE_M}{2\pi X}F(\psi, \theta)$$

provided that the reactive voltages accompanying conduction in one anode do not affect conduction by any other anode. This condition is satisfied if all the inductance is in the anode leads. It is also satisfied if the conducting period of an anode is less than $2\pi/q$.

Reference

1. Analysis of Rectifier Circuits, E. F. Christensen, C. H. Willis, C. C. Herskind. AIEE Transactions, volume 63, 1944, pages 1048–55.

No Discussion

Doubly Chorded or Doubly Shifted Fractional-Slot Lap and Wave Windings

M. M. LIWSCHITZ
MEMBER AIEE

THE PATTERN used most frequently for laying out balanced fractional-slot windings is that for maximum distribution factor of the main wave. From this pattern, a large number of other balanced patterns can be obtained by a cyclic shifting of coils from one phase to the other inside the same repeatable group. The distribution factor of the main wave of these latter patterns is always smaller than that of the former pattern. The distribution factors of the harmonics are partially smaller, partially larger than those of the former pattern, so that the values of the harmonic (differential) leakage of the different patterns do not differ much from each other. The advantage of the pattern with a cyclic shift of coils inside the same repeatable group is that it makes it possible to reduce the distribution factors of one or several undesirable electromotive force or magnetomotive force harmonics. The aim of this paper is to show that the same effect of reduction of certain harmonics can be achieved by shifting of repeatable groups with respect to each other. This method of reduction of harmonics has the advantage that it is applicable to lap as well as to wave windings, while the cyclic shift of coils is limited to lap windings. Further, the shift between the repeatable groups necessary to achieve desirable results is seen immediately, while with the cyclic shift of coils inside the same repeatable group, the trial and error method must be applied. On the other hand, the shifting of repeatable groups reduces the maximum possible number of parallel circuits.

The normal chording of a winding is nothing more than a shift of the lower layer with respect to the upper layer. When repeatable groups are shifted with respect to each other, the fractional-slot winding becomes doubly shifted or doubly chorded and two pitch factors are to be considered. In many practical cases, it becomes necessary to reduce several harmonics simultaneously. In these cases, the double chording and cyclic shift of coils can be combined.

Cyclic Shift of Coils Inside the Same Repeatable Group of a Lap Winding

Figure 1 shows the slot star[1] of a 3-phase 20-pole lap winding with $q=N/\beta=17/5$ (for symbols see List of Symbols). There are 17 slots per phase in five-poles. For maximum distribution factor with respect to the main wave, the first 17 slots (1, 42, 32 . . . . 14, 4, 45) are assigned to phase A; the following 17 slots (35, 25, 15 . . . . 48, 38, 28) to phase C; and the last 17 slots to phase B. The slots assigned to phase A are indicated by crosses; those assigned to phase C by rectangles; and those assigned to phase B by circles.

Figure 2 shows the slot star for the same winding with slots 4, 38, and 21 subjected to a cyclic shift. Slot 4, which in Figure 1 belongs to phase A, is shifted to phase C; slot 38, which in Figure 1 belongs to phase C, is shifted to phase B; and slot 21, which in Figure 1 belongs to phase B, is shifted to phase A.

The distribution factors of the main wave and of a part of the harmonics are as shown in Table I.

$n'=p=10$ is the main wave. The distribution factor of this wave is reduced slightly by the cyclic shift, while for example, the distribution factor of the wave of the order $n'=14$, which is the 1.4th harmonic with respect to the main wave is reduced by 77.4 per cent. If there is a danger that an 8-pole force wave, produced by the main wave and the wave of the order $n'=14$, may produce a 120-cycle vibration and noise, the cyclic shift of coils inside the same repeatable group proves very useful. The main wave produces an 8-pole force wave also with the harmonic of the order $n'=6$, the distribution factor of which is slightly increased by the cyclic shift of coils. However, the pitch factor of the latter harmonic is smaller than that of the wave $n'=14$, and it is also more damped by the rotor than the wave of the order $n'=14$.

Shift of Repeatable Groups with Respect to Each Other

The shift of repeatable groups with respect to each other does not affect the distribution factors, but introduces a second pitch factor. The first pitch factor is due to the fractional coil pitch.

The angle between two adjacent vectors of the slot star is

$$\alpha_m=\frac{180^\circ}{mN} \tag{1}$$

If the lower layer is shifted with respect to the upper by k_1 angles α_m, the first pitch factor, due to fractional coil pitch is

$$K_{1pn}'=\text{Cos } k_1(\alpha_n'/2) \tag{2}$$

and, if the repeatable groups are shifted with respect to each other by k_2 angles α_m the second pitch factor is

$$K_{2pn}'=\text{Cos } k_2(\alpha_n'/2) \tag{3}$$

The total pitch factor of the winding is

$$K_{1pn}'\times K_{2pn}'$$

If in the equation

$$d=\frac{mNP+1}{\beta} \tag{4}$$

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M. M. LIWSCHITZ is a professor with the Polytechnic Institute of Brooklyn, Brooklyn, N. Y., and also is connected with the Westinghouse Electric Corporation, East Pittsburgh, Pa., as consulting engineer.

Table I

$n'=$	2	6	10	14	22	26	30	50	70	110	130	170	
$K_{dn'}=$	0.031	0.104	0.955	0.053	0.031	0.045	0.638	0.191	0.138	0.090	0.076	0.156	without cyclic shift
$K_{dn'}=$	0.081	0.116	0.951	0.012	0.084	0.031	0.616	0.173	0.113	0.051	0.030	0.235	with cyclic shift

P is the smallest integer which makes d an integer, then,

$$\alpha_n' = n'd\alpha_m \frac{\beta}{p} \text{ when } P \text{ is even}$$

$$\alpha_n' = n'd\alpha_m \frac{\beta}{p} + 180° \text{ when } P \text{ is odd and } \beta \text{ is even}$$

$$\alpha_n' = n'(d\alpha_m + 180)\frac{\beta}{p} \text{ when } P \text{ is odd and } \beta \text{ is odd} \quad (5)$$

The harmonics which occur in 3-phase windings with no zero sequence currents are

$$n' = \frac{p}{\beta}\gamma \quad \gamma = 1, 5, 7, 11, 13 \ldots \quad (6)$$

when β is odd, and,

$$n' = \frac{2p}{\beta}\gamma \quad \gamma = 1, 2, 4, 5, 7, 8 \ldots \quad (7)$$

when β is even.

Considering the wave of the order $n'=14$ of the previously treated lap winding (Figure 1), a value of k_2 is to be determined which makes this harmonic as small as possible. For this winding

$$d = \frac{3\times 17\times P+1}{5} = 41 \text{ with } P=4$$

$$\alpha_n' = n'\times 41\times\frac{180}{3\times 17}\times\frac{5}{10} = n'\times 72.35°$$

Hence, for the wave of the order $n'=14$

$$\alpha_{(n'=14)} = 14\times 72.35° = 293° \text{ and}$$
$$\alpha_n'/2 = 146.5°$$

With $k_2=3$, the second pitch factor becomes

$$K_{p(n'=14)} = \cos 79.5° = 0.182$$

Comparing the wave $n'=14$ of the cyclic shift of coils with that of the shift of repeatable groups, the result is

$$K_{d(n'=14)} = 0.012 \text{ with cyclic shift of coils}$$

$$K_{d(n'=14)}\times K_{p(n'=14)} = 0.053\times 0.182 = 0.0096 \text{ with shift of repeatable groups}$$

Applying both shifts,

$$K_{d(n'=14)}\times K_{p(n'=14)} = 0.012\times 0.182 = 0.0022$$

and the wave $n'=14$ becomes very small.

The first pitch factor is independent of both kinds of shift. If the coil width is equal to eight slot pitches, the value of k_1 is equal to 11.

The slots which are to be assigned to the three phases for the shift $k_2=3$ can be found from the slot star or simpler from the d-series which the vectors of the slot star follow. This series or the slot star, Figure 1, yields for the first repeatable group of phase A, the slots

1	42	32	22	12	2	43	33	23	13
				3	44	34	24	14	4	45

When there is no shift between the repeatable groups, the second repeatable group of phase A consists of the slots

52	93	83	73	63	53	94	84	74	64
			54	95	85	75	65	55	96

In order to produce a shift of $k_2=3$ angles α_m between the first and second repeatable groups, the slots of the second repeatable group assigned to phase A must be

73	63	53	94	84	74	64	54	95	85
			75	65	55	96	86	76	66

that is, the first three slots of phase C are to be assigned to phase A, the first three slots of phase B are to be assigned to phase C, and the first three slots of phase A to phase B. The same applies to the fourth repeatable group with respect to the third.

Due to the shift of repeatable groups, the maximum possible number of parallel circuits of the winding considered is reduced from four to two. In general, there will be maximum possible p/β parallel circuits instead of $2\,p/\beta$.

Since the influence of the shift of repeatable groups appears in the form of a pitch factor, all harmonics are reduced by this shift. However, in the case when β equals an odd number, the shift of repeatable groups introduces even harmonics.

The method of shifting repeatable groups will be demonstrated also on a wave winding. It is shown in a companion paper[3] that a balanced wave winding can be made for the same fractional number of slots per pole per phase for which a balanced lap winding is possible. The investigation of the wave windings, as well as that of the lap windings, is here based upon the slot star or, what is the same, upon the d-series.

A 3-phase 10-pole wave winding with $q=11/5$ will be considered. For this winding, $N=11$, $\beta=5$, $d=20$, $y=13$, and $a=-1$. The winding goes opposite to the numbering of the slots. With two conductors per slot and left-hand coils, the lower conductors shown in Table II are assigned to the three phases when there is no shift of repeatable groups.

Each phase consists of two waves with the same number of coils. When the two repeatable groups are shifted with respect to each other by $k_2=3$ angles α_m, the conductors assigned to the three phases are as shown in Table III.

Each phase consists of two waves with unequal numbers of coils which must be connected in series.

Figure 1. Slot star of a 3-phase 20-pole winding with q=17/5 or 3⅗

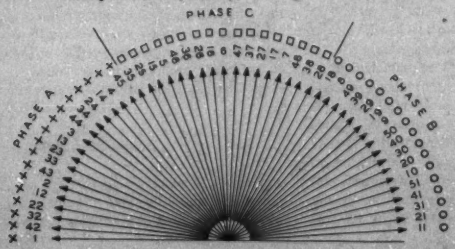

Figure 2. Slot star of the 3-phase 20-pole winding shown in Figure 1, but with slots 4, 38, and 21 subjected to a cyclic shift

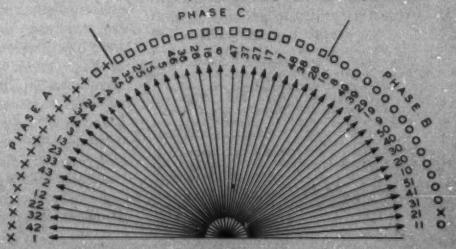

Table II

1	54	41	28	15	2	55	42	29	16	3	Phase A
34	21	8	61	48	35	22	9	62	49	36	
23	10	63	50	37	24	11	64	51	38	25	Phase C
56	43	30	17	4	57	44	31	18	5	58	
12	65	52	39	26	13	66	53	40	27	14	Phase B
45	32	19	6	59	46	33	20	7	60	47	

Table III

1	54	41	28	15	2	55	42	29	16	3	56	43	30	Phase A
			61	48	35	22	9	62	49	36				
23	10	63	50	37	24	11	64	51	38	25	12	65	52	Phase C
			17	4	57	44	31	18	5	58				
			39	26	13	66	53	40	27	14				Phase B
45	32	19	6	59	46	33	20	7	60	47	34	21	8	

List of Symbols

a = number of angles α_m between the vectors x and $x+y$ of the slot star

d = difference between two slots which correspond to two adjacent vectors of the slot star

k_1 = number of angles α_m by which the lower layer is shifted with respect to upper layer

k_2 = number of angles α_m by which two repeatable groups are shifted with respect to each other

K_{1pn}' = pitch factor of the n'-th harmonic due to fractional coil pitch (first pitch factor)

K_{2pn}' = pitch factor of the n'-th harmonic due to shift of repeatable groups (second pitch factor)

m = number of phases

n' = order of the harmonic with respect to a fundamental whose wave length is equal to the circumference of the armature

N = numerator of the fraction which fixes the number of slots per pole per phase

$q \times \beta$ = number of slots per phase per repeatable group

p = number of pole pairs

P = number of full pole pitches between two slots which correspond to two adjacent vectors of the slot star

$q = N/\beta$ = number of slots per pole per phase

y = winding pitch

$\alpha_m = 180/mN$ = angle between coils with respect to main wave

α_n' = angle between coils with respect to the n'-th harmonic

β = denominator of the fraction which fixes the number of slots per pole per phase

References

1. Distribution Factors and Pitch Factors of the Harmonics of a Fractional-Slot Winding, M. M. Liwschitz. AIEE Transactions, volume 62, 1943, pages 664–6.

2. Balanced Fractional-Slot Wave Windings, M. M. Liwschitz. AIEE Transactions, volume 67, 1948, pages 676–8.

No Discussion

Steady-State Stability of Synchronous Machines as Affected by Angle-Regulator Characteristics

C. CONCORDIA
FELLOW AIEE

THERE has recently been some discussion of the benefits (with respect to electric power system stability) to be obtained by the use of a properly designed angle regulator.[1-5] An angle regulator applied to a synchronous machine is defined here as a regulator that varies the machine excitation voltage in response to changes in the angle between the rotor interpole axis and the effective system voltage so as to tend to restore the initially set angle. The purpose of this paper is to present the results of an analytical exploration of the possibilities of the angle regulator as regards stability. In particular, since voltage regulators are already extensively used, and since their use as aids in power-system stability has already been studied,[6,7,8] it has seemed appropriate to compare the action of an angle regulator with that of a voltage regulator.

Conclusion

The principal conclusion of the present analysis is that the dynamic actions of the angle and voltage regulators near the system stability limit are essentially equivalent, so that their effects on stability can by proper regulator design be made practically indistinguishable. The need for an angle regulator cannot therefore be demonstrated on the basis of stability requirements.

General Discussion

In the normal operation of a synchronous generator over its load range it is customary, and indeed it seems highly desirable and almost necessary, to maintain voltage, rather than load angle constant. Thus if an angle regulator were used it would have to be biased by a voltage regulator at least in the steady state. If it appears worth while to have temporarily, a somewhat higher excitation than would result from the normal action of a voltage regulator (as was true in some of the disturbances studied in connection with reference 8), it seems at least as simple to accomplish this by temporarily modifying the action of the voltage regulator as by introducing the concept of holding angle.

The conclusion that voltage and angle regulators are dynamically practically indistinguishable is based upon an analysis some of the results of which will be presented and discussed later. However, it may be worth while to advance first an argument based on physical considerations. A simple case of an unregulated synchronous machine connected through a tie line to an infinite bus, or two machines connected together and transferring power, may be easily visualized. It is evident that as the machines swing against each other the variation of terminal voltage and machine rotor angle occur very nearly in phase, the angle increasing as the voltage decreases. Armature and amortisseur transients are sufficiently fast to be neglected, so that the only shift in phase is produced by the field resistance or finite field time constant. For an abrupt increase in angle, the terminal voltage will first decrease as determined by the machine transient reactances, and then decrease still more to the value determined by the synchronous reactances. For the usual frequencies of oscillation (greater than a tenth of a cycle per second), the phase shift is very slight and the magnitude of the voltage variation may be computed approximately from the transient reactances. However, this slight phase shift is found to be beneficial, as the present analysis has shown that somewhat less damping torque (as produced by local load or the machine amortisseur, or by introducing response to the rate of change of voltage or angle) is required for the voltage regulator than for the angle regulator. Another difference in performance over the load range is that as the operating angle increases the voltage drop for a given angle increase increases, so that the voltage regulator tends to act more and more in response to power-input or system changes as the stability limit is approached. This effect is illustrated for a simple case in Figure 1.

Results of Analysis

Figure 2 shows some of the results of calculations of the stability limits as affected by an angle regulator of the simple form

$$\Delta E = \frac{a_2}{T_r p + 1} \Delta \delta \qquad (1)$$

where

E = generator field voltage
δ = generator angle relative to infinite bus
a_2 = regulator amplification factor
T_r = regulator (or exciter) time constant

The system considered is the same as that for which the effects of a voltage regulator of similar form were shown in Figures 1 and 2 of reference 7, and consists of a synchronous machine of unit synchronous reactance connected to an infinite bus through a tie line of unit reactance, with the machine terminal voltage and power

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C. CONCORDIA is with the central station engineering division of the General Electric Company, Schenectady, N. Y.

All the stability calculations for the angle-regulator system discussed in this paper were made by Miss M. V. Barry.

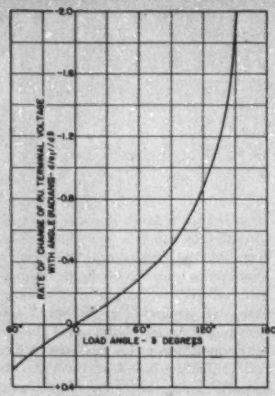


Figure 1. Effect of load angle δ on change of terminal voltage e_t with angle—synchronous machine and tie line

$X_{machine} = X_{line}$
$E_{machine} = e_{line}$
$e_{to} = PF_{terminal} = 1.0$

factor maintained at unity in the steady state. Figure 1 of reference 7 is repeated here as Figure 3 for ready comparison.

Figures 2 and 3 show that the maximum gains in power limit (for all these cases the power P is simply $P = \tan \delta/2$ so that for example an angle δ of 110 degrees corresponds to a power of 1.43 per unit) are identical for either regulator. The shapes of the curves are also practically the same, the range of permissible amplification (expressed as the ratio of maximum to minimum amplification) being only very slightly less for the angle regulator than for the voltage regulator. For example, on curve C at $\delta = 100$ degrees the permissible range of a_1 (the voltage regulator amplification factor) is from -0.54 to -19 (a range of 35 to 1), while the corresponding range of a_2 (the angle regulator amplification factor) is from $+0.27$ to $+7.6$ (a range of 28 to 1).

The actual magnitude of the power gain depends in any case on the system conditions, in particular on the relative importance of the machine reactance and the tie-line reactance. In the interpretation of Figures 2 and 3 it is convenient to note[7] that the maximum power limit for curve A of both figures is simply that which would be calculated by using machine transient reactance instead of synchronous reactance (using constant field flux linkages instead of constant field current). In long-distance power transmission the machine reactance may be relatively smaller so the percentage gains in power limit would be correspondingly smaller, but would still be appreciable and worthy of consideration.[8]

The concept of using transient reactance provides a quick means for estimating the gain which may be obtained by use of a proper regulator, as both calculations and tests have shown that the effects may be approximated by using a reduced machine reactance which may approach the transient value.

Figure 4 shows the effect of adding a slip-torque damping term $T_d p\delta$ to the torque equation of the synchronous machine, as may arise from the amortisseur, prime mover torque-speed characteristic, or local load. There is no effect on the maximum increase in power obtainable by the angle regulator, but the stable region is considerably extended at light load. In the case of a voltage regulator, the stability limit appears more or less like curve B of Figure 4 even for no slip-torque (in part because of the voltage-angle characteristic of Figure 1), while the addition of slip-torque produces an additional stable region at very high regulator amplification factors which may reach greater power limits, as illustrated in Figures 10 and 11 of reference 8 and in Figure 5 of this paper.

The effect of introducing a signal into the angle regulator proportional to slip, or rate of change of angle, $p\delta$ was studied by modifying the regulator Equation 1 to the form:

$$\Delta E = \frac{1}{T_r p + 1}(a_2 p\delta + a_2 \Delta\delta)$$
$$= \frac{kp+1}{T_r p + 1} a_2 \Delta\delta \qquad (2)$$

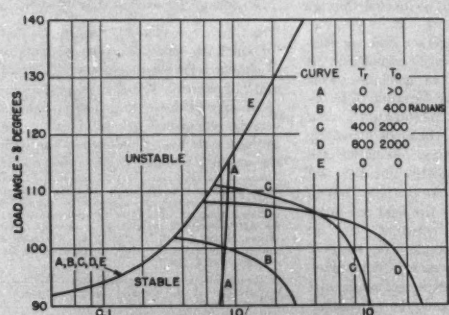
where

$$k = a_3/a_2 \qquad (3)$$

It is evident that by making $k = T_r$, all cases are made equivalent to the case $T_r = 0$ or no time lag in the exciter. This permits the maximum operating angle to approach 115.5 degrees as shown in Figure 2, curve A but also reduces considerably the permissible range of amplification factor. Moreover calculations have shown that as long as the exciter time constant T_r is appreciably greater than zero, making $k = T_r$ produces the maximum gain. As k varies between zero and T_r the maximum gain increases slightly and the permissible range of amplification factor a_2

Figure 2. Effect of angle regulator on steady-state stability limit—synchronous machine and tie line

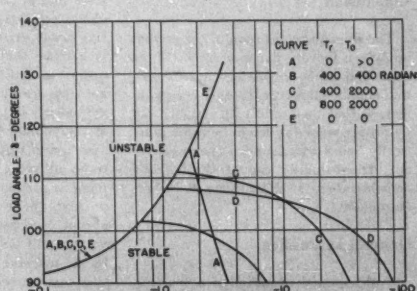
$x_d' = 0.3$ $\quad r = 0$
$x_d = x_q = x = 1.0$ $\quad M = 4{,}000$ radians
$PF_{terminal} = e_{terminal} = 1.0$



a_2—ANGLE REGULATOR AMPLIFICATION FACTOR
P.U. CHANGE IN FIELD VOLTAGE PER RADIAN CHANGE IN ANGLE.

Figure 3. Effect of voltage regulator on steady-state stability limit—synchronous machine and tie-line

$x_d' = 0.3$ $\quad r = 0$
$x_d = x_q = x = 1.0$ $\quad M = 4{,}000$ radians
$PF_{terminal} = e_{terminal} = 1.0$



a_1-VOLTAGE REGULATOR AMPLIFICATION FACTOR
P.U. CHANGE IN FIELD VOLTAGE PER UNIT CHANGE IN TERMINAL VOLTAGE.

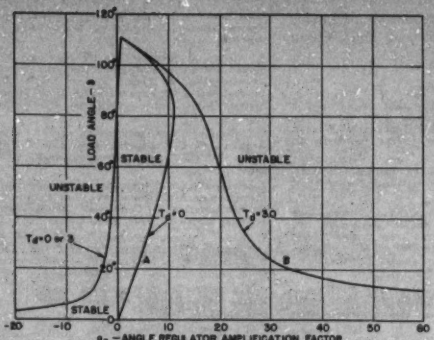


Figure 4. Steady-state stability limit with angle regulator—effect of damping torque coefficient T_d—synchronous machine and tie line

$T_r = 400$ radians $\quad x_d = x_q = x = 1.0$
$T_o = 2{,}000 \quad e_{to} = PF_{to} = 1.0$
$M = 4{,}000 \quad r = 0$
$x_d' = 0.3$

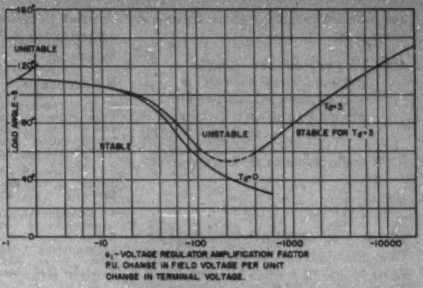


Figure 5. Steady-state stability limit with voltage regulator—effect of damping torque coefficient T_d—synchronous machine and tie line

$T_r = 400$ radians $\quad x_d = x_q = x = 1.0$
$T_o = 2{,}000 \quad e_{to} = PF_{to} = 1.0$
$M = 4{,}000 \quad r = 0$
$x_d' = 0.3$

decreases. When k is made greater than T_r the maximum gain decreases again and the permissible range also decreases. If the exciter time lag T_r is extremely small or zero, considerable gains can be made by introducing the response to slip, but this would be difficult to realize practically. Thus in the practical range the effect of response to slip appears to be unimportant.

Figure 6 shows the effect of the angle regulator operating in combination with a voltage regulator. Both regulators have the same time lag, as this lag has been assumed to occur principally in the exciter rather than in the regulator proper.

The regulator equation is of the form

$$\Delta E = \frac{1}{T_r p + 1}(a_1 \Delta|e_t| + a_2 \Delta\delta)$$

where a_1 is the voltage regulator amplification factor, and $|e_t|$ is the absolute magnitude of the generator terminal voltage. Figure 6 shows that the only effect of adding the voltage regulator is to change the required angle-regulator amplification factor, the maximum gain being unaffected and occurring when

$a_2 \cong {}^1/_2 a_1$

Analysis

The power system considered consists of a synchronous machine connected through a tie line to an infinite bus. The regions of stable operation in the steady state have been determined by examining the equations for small displacements of this machine from its equilibrium point for various loadings, always starting from unit voltage and power factor at the terminals of the machine.

Referring to reference 7, equations 1–15 and 30–32, and using the same notation and assumptions one may write the following equations for changes of voltage:

For the machine:

$$\Delta e_d = x_q \Delta i_q \tag{4}$$

$$\Delta e_q = G(p)\,\Delta E - x_d(p)\,\Delta i_d \tag{5}$$

where

$$G(p) = \frac{1}{T_o p + 1} \tag{6}$$

$$x_d(p) = \frac{x_d' T_o p + x_d}{T_o p + 1} \tag{7}$$

e_d, e_q = direct- and quadrature-axis components of machine terminal voltage
i_d, i_q = direct- and quadrature-axis components of armature current
T_o = machine open-circuit field time constant
x_d, x_d' = machine synchronous and transient reactances respectively

Equations 6 and 7 are for a machine with no amortisseur. For the tie line:

$$\Delta e_d = -x\,\Delta i_q + e \cos\delta\,\Delta\delta \tag{8}$$

a_1 = amplification factor of voltage regulator

$T_r = 400$ radians
$T_o = 2{,}000$
$M = 4{,}000$
$x_d = x_q = x = 1.0$
$e_{to} = PF_{to} = 1.0$
$r = 0$
$x_d' = 0.3$

$$\Delta e_q = x\,\Delta i_d - e \sin\delta\,\Delta\delta \tag{9}$$

where

x = tie-line reactance, the tie-line resistance is zero
e = infinite-bus voltage
δ = angle between the infinite bus voltage and the machine no-load voltage

The machine torque equation is:

$$\Delta T = i_{qo} G(p)\,\Delta E - (\psi_{do} + i_{qo} x_d(p))\,\Delta i_q + (\psi_{do} + i_{do} x_q)\,\Delta i_q + M p^2 \Delta\delta \tag{10}$$

where

M = moment of inertia of the machine rotor

If the possibility of using both a voltage and an angle regulator in combination is considered, the regulator equation becomes (see equation 7 of reference 7):

$$\Delta E = g_1(p)(e_d'\,\Delta e_d + e_q'\,\Delta e_q) + g_2(p)\,\Delta\delta \tag{11}$$

Using these equations and the relations 31 of reference 7 for the steady-state quantities under the given terminal conditions and with $x_d = x_q = x = 1.0$, the char-

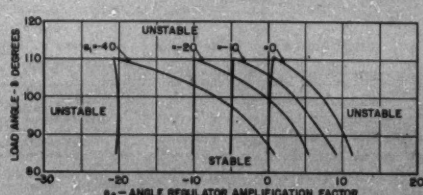


Figure 6. Steady-state stability limit with angle regulator—effect of adding voltage regulator—synchronous machine and tie line

acteristic determinant of the coefficients is found to be:

$$p^{3}2MT_{a}(1+x_{d}')\cos^{2}\delta/2+p^{2}2M(2-g_{1}(p)\cos\delta/2)\cos^{2}\delta/2+p^{1}T_{a}\times[(1+x_{d}')\cos\delta+(1-x_{d}')\sin^{2}\delta]+p^{0}(2\cos\delta-g_{1}(p)\cos\delta/2+2g_{2}(p)\times\sin\delta\cos\delta/2) \quad (12)$$

If the angle regulator alone is considered

$$g_{2}(p)=\frac{a_{2}}{T_{r}p+1}$$

according to equation 1, and $g_1(p)=0$. Then the determinant becomes of the fourth degree in p when cleared of fractions. The limits of stability are determined by application of Routh's stability criteria[9] to this determinant for various operating angles δ and amplification factors a_2.

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No Discussion

Performance Calculation of Induction-Type Frequency Converters

FREDERICK R. LATZKO
MEMBER AIEE

Synopsis: The performance of induction-type frequency converters is reviewed. A design method is given, based on the equivalent circuit. The current locus, with varying loads, is derived.

THE INTRODUCTION of high-speed motors for grinding spindles and for other industrial applications, on an ever increasing scale, brought with it an increased demand for induction-type frequency converters as a very practical solution for a source of high-frequency power supply. As the specifications vary considerably, in each individual case, a simple and reliable method for selecting and designing this type of equipment is important. The following analysis deals with frequency conversion, in general, above and below line frequency.

The Frequency Conversion

Figure 1(*A*) illustrates a simplified connection diagram of a frequency converter, for adjustable frequency, by means of a Ward-Leonard control. Figure 1(*B*) and (*C*) demonstrates the principle features of a converter, coupled to an a-c drive, providing single frequency only. It is beyond the scope of this paper to deal with all possible connection arrangements and their implications.

In its general construction, the frequency converter is identical with a standard slip-ring motor. With the primary winding connected to the available line, for instance, of 60 cycle, a voltage of line frequency can be measured across the secondary terminals at standstill (slip = 1). Driving the shaft of the frequency converter at some speed, either with rotation or against rotation of the revolving field, changes the voltage and frequency across the secondary terminals, as the relative speed of this field, in respect to the secondary winding, has changed. Frequency, and practically the voltage, also, are in direct proportion to this relative shaft speed, in other words, to the slip. The interrelationship between secondary frequency, shaft speed, and synchronous speed (=speed of revolving field), is graphically shown in Figure 2, and is given by:

$$\pm f_2/f_1 = \pm S \qquad (1)$$

$$n = [1 - (\pm S)]n_s \qquad (2)$$

For positive slips, larger than unity, the shaft speeds become negative, denoting a rotation against the revolving field. It may be noted that the phase sequence on the secondary terminals reverses in passing through synchronism. Disregarding the sign, a definite frequency always can be obtained at two different shaft speeds of the same converter, corresponding to a positive and to a negative slip of the same absolute value.

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FREDERICK R. LATZKO is with the engineering department of the Electric Specialty Company, Stamford, Conn.

Selection of the Frequency Converter

The frequency converter must be selected in accordance with primary frequency, secondary frequency, shaft speed, and number of poles. These quantities are interrelated by the formula:

$$f_1 \mp f_2 = Pn/120 \qquad (3)$$

This function is plotted in Figure 3 for numbers of poles, from 2 to 20, and, for shaft speeds up to 4,800 rpm. The minus sign has to be used with positive slips and vice versa. For example, to obtain 420 cycle from a 60 cycle line, draw a line parallel to the abscissa, with $f_1 - f_2 = -360$ and with $f_1 + f_2 = 480$, and tabulate the values, on the abscissa, of all points of intersection, with the sloping lines:

Number of poles	20	18	16	14	12	10
Shaft speed ($f_1 - f_2 = -360$)	−2,160	−2,400	−2,700	−3,086	−3,600	−4,320
Shaft speed ($f_1 + f_2 = 480$)	2,880	3,200	3,600	4,114	4,800	

It is desirable to use the frequency converter with the smallest number of poles coupled directly to the driving motor. In the foregoing case, this condition is fulfilled with a 12-pole converter, driven at 3,600 rpm, against field rotation by a 2-pole motor. For an easy determination, whether direct coupling is feasible, Figure 3 shows, in heavier print, lines parallel to the abscissa, for the most common line frequencies of 60, 50, and 25 cycle. Their intersection, with the sloping lines, indicate the available shaft speeds for these frequencies.

Selection of the Driving Motor

It should be noted that an induction-type frequency converter may absorb or deliver power through its primary terminals, secondary terminals, and its shaft. The law of conservation of energy requires that the algebraic sum of all three powers, and of the losses of the converter

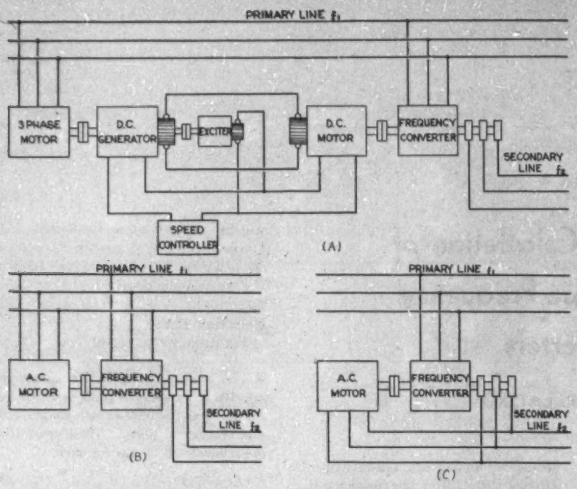

Figure 1. Simplified connection diagram

(A) for adjustable frequency
(B), (C) for single frequency

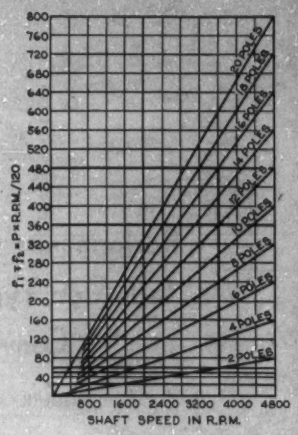

Figure 3. Chart for selecting frequency converters

itself, must be zero. The driving motor has to be selected in accordance with the mechanical power at the shaft of the converter, the accurate determination of which will be shown in connection with the design procedure. However, a general idea of the balance of the three powers may be obtained by considering an ideal converter without losses and without magnetizing current. For such a converter, the following equations apply:

$$P_1 = \pm(f_1/f_2)P_2 \tag{4}$$

$$P_{1M} = \pm(f_1/f_2)P_{2M} \tag{4a}$$

$$P_m = P_{2M} - P_{1M} = P_{2M}(1 \mp f_1/f_2) \tag{5}$$

$$P_m/P_{2M} = 1 \mp f_1/f_2 \tag{5a}$$

Figure 4 shows a graph of equation 5a. The ordinate from any point on the abscissa to either sloping line is a measure of the mechanical power at the shaft for the given ratio $\pm f_1/f_2$. It can be seen that operating at positive slips always gives smaller mechanical power (AB < AC A′B′ < B′C′). It can be shown that the two different mechanical powers, with which the identical secondary frequency may be obtained, are in direct proportion to the two different shaft speeds. For secondary frequencies, smaller than the primary frequency, $(f_1/f_2 > 1)$, and operating at a positive slip, the mechanical power becomes negative, which means that the converter delivers mechanical power to the driving motor, which in turn, becomes a generator feeding electrical power into the line. The larger mechanical power required, operating in the range of negative slip, is due to the fact that the primary power becomes negative (see equation 4 and 4a), feeding electric power into the primary line. Thence, primary and secondary power must be provided mechanically through the shaft.

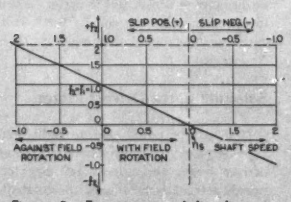

Figure 2. Frequency-speed-slip diagram in per-unit values

$(N_s = 1, \quad f_1 = 1)$

The Performance of the Frequency Converter

It is well known that the frequency converter belongs to the category of the generalized a-c transformer. The current in the secondary reacts upon the primary exactly as in the case of the standard induction motor. One can actually consider the frequency converter as a slip ring motor compelled to run with a slip, $S = \pm f_2/f_1$, with an external impedance in series with the secondary winding. The current in the secondary winding develops a torque in interaction with the air gap flux. It is the task of the machine, mechanically connected to the shaft of the frequency converter, either to absorb this torque, or to overcome it, whichever the case may be.

Figure 5 shows the equivalent circuit of the frequency converter, which differs from the equivalent circuit of an induction motor only by the load impedance in series with the secondary circuit. It is convenient to calculate the constants of the converter for the primary frequency, f_1. The referring of the secondary constants r_2 and x_2, to the primary, is done in the same manner as in the case of an induction motor. Both resistance and reactance must be multiplied with an impedance reduction factor, a_g. In addition to it the resistance must be divided by the slip or, as an equivalent, multiplied with $a_F = \pm f_1/f_2$. The load impedance has to be established from the given secondary output conditions:

$$Z_L = \frac{m_2 E_2^2}{P_2} \tag{6}$$

$$r_L = Z_L \cos\theta_2 \tag{7}$$

$$X_L = Z_L \sin\theta_2 \tag{8}$$

The load resistance is referred to the primary by multiplying r_L with the same combined reduction factors, $a_F a_g$, as before. However, in referring the load reactance, it has to be noted that X_L in (8)

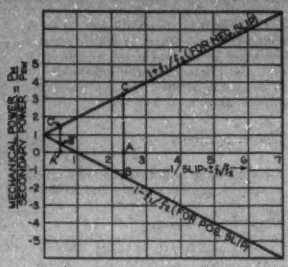

Figure 4 (left). Ratio of mechanical power to secondary power

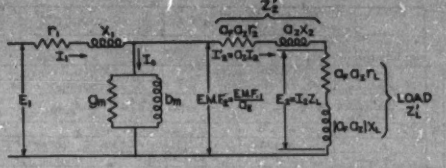

Figure 5. Equivalent circuit

is given in terms of the secondary frequency f_2. It has first to be reduced to line frequency f_1 by multiplying it with f_1/f_2. This factor equals $|a_p|$, giving, for the load reactance, the combined reduction factor $|a_p a_2|$.

A clear conception of the general performance of an induction type frequency converter can be obtained by drawing the circle diagram of a fictitious induction motor with a rotor resistance $R_2 = r_2 + r_L$, and a rotor reactance $X_2 = x_2 + |a_p| X_L$ compelled to run at slips, $S = \pm f_2/f_1$. Figure 6 shows such a diagram. The two points, A and A′, represent the load points running at a slip $S = \pm 25/60 = \pm 0.417$, corresponding to a frequency conversion from 60 to 25 cycle. They were determined by means of the slip line. The interesting quantities can be read from the circle diagram in the same manner as in the case of a standard induction motor. The following tabulation may be helpful in understanding the performance:

Running at Positive Slip

AE—Total electric power absorbed through the primary terminals

AB—Mechanical power delivered at the shaft

BD—Electric power absorbed in the secondary circuit (secondary winding and load)

DE—Losses in primary circuit and core losses

Running at Negative Slip

A′E′—Electrical power delivered through the primary terminals

A′B′—Total mechanical power absorbed through the shaft

B′D′—Electric power absorbed in the secondary circuit (secondary winding and load)

D′E′—Losses in primary circuit and core losses

It may be noted that BD or B′D′ represent the total electric power absorbed in the secondary. Drawing a line, which intersects BD and B′D′ in the ratio r_2/r_L, permits the separation of the rotor copper losses BC or B′C′ from the useful secondary output CD or C′D′.

Any change in load represents a change in the value of the impedance Z_L, in series with the secondary winding. To find the new load points, would require to develop a new circle diagram, and again, to find on it the load points for $S = \pm 0.417$. In this way, one could develop, point by point, the current locus for changing loads. It is shown, in the Appendix, that this current locus is represented by a family of circles, each power factor load having a locus of two circles, one for $S = +f_2/f_1$, and one for $S = -f_2/f_1$. Common points of this family of circles can be found by solving the equivalent circuit, Figure 5, for $Z_L = \infty$, and $Z_L = 0$.

Design Procedure

The performance calculation will be explained in reference to the design sheet in Figure 7, which shows a complete sample design. As a first step, it will be possible, in most cases, to select a frame size, based on its rotor data, as a plain slip ring motor with due consideration to the actual frequency conditions. Assigning a suitable air gap flux, estimating the voltage regulation, and selecting the coil pitch, fixes completely the primary and secondary windings for a first trial design.

All the machine constants, based on line frequency, the no load current, core losses, and reduction factors, can then be calculated, and their values inserted in the proper places on the design sheet. The calculation of the load constants, based on secondary frequency, was described before. Line 1 to 9 gives the well-known calculation of the combined impedance of the equivalent circuit in Figure 5. The resulting primary current is calculated in line 10. The secondary current, in line 14, is calculated from the primary $EMF/_{1FL}$, acting on the total secondary impedance Z'_{2L}, expressed in actual secondary terms by dividing by a_l. The primary EMF_{1FL} is computed as the difference between the applied primary voltage E_1, and the drop $I_1 Z_1$, in lines 11 to 13, or by calculating I_1/Y_{2LM}. The secondary voltage, across the load, is calculated in line 15. The voltage regulation, in line 17, requires the knowledge of the *EMF*, under no load conditions. This calculation is performed in the same manner as before, for full load conditions introducing, in lines 18 to 21, the no load values rather than the full load values, or by calculating I_0/Y_m. It should be noted that the design started with an estimated voltage regulation, implying an estimated primary and secondary voltage drop.

As the actual voltage drop may differ from the estimated one, the values I_2, E_{2FL}, and P_2, in lines 14 to 16, may, on the first try, disagree with the specification. It may be, therefore, necessary to repeat the design with some other value, $\lambda Z_L = \lambda r_L + j\lambda X_L$, by multiplying the values in line 2 by λ, until a P_2—value is obtained close enough to the specified one. However, it may be that the values I_2, and E_{2FL}, with the correct P_2—value, may still differ from the specification. All that would be required, in this case, is to change the number of secondary turns in ratio of the desired full load voltage to the calculated one, provided the voltage regulation is satisfactory. If the latter is not the case, the voltage drop must be improved, which may necessitate the use of a larger frame.

As a last step in the performance calculation, the mechanical power, at the shaft, must be determined. For this purpose, the secondary watt output, and all the losses are added up, assigning to each item, the negative sign. (See lines 22 to

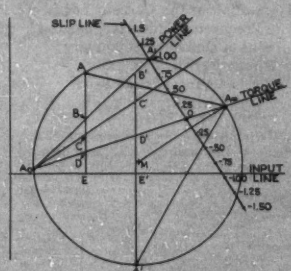

Figure 6. Circle diagram

	STATOR OR ROTOR	f_1/f_2	POLES 3PEED	KVA-R.F.	m_1/m_2	CONN.	LINE VOLTAGE	E_1/E_2	N_1/N_2	K_{dp1}/K_{dp2}
PRIMARY	STATOR	60	12	—	3	Y	440	254	1152	0.945
SECONDARY	ROTOR	410	3500	100 0.8	3	Y	264	152.5	132	0.979

CONSTANTS

$a_E = \frac{m_1}{m_2}\frac{N_1K_{dp1}}{N_2K_{dp2}}^2 = \frac{3}{3}\frac{(1152)(0.945)}{(132)(0.979)}^2 = 71$ $\qquad a_P = \pm f_1/f_2 = +60/410 = 0.1463$

$a_F = a_E \pm f_1/f_2\, a_I = +0.1463(71) = 10.38$

$a_I = \frac{m_2}{m_1}\frac{N_2K_{dp2}}{N_1K_{dp1}} = \frac{3}{3}\frac{(132)(0.979)}{(1152)(0.945)} = 0.1187$ $\qquad a_E = \frac{N_1K_{dp1}f_1}{N_2K_{dp2}f_2} = \frac{1152(0.945)60}{132(0.979)410} = 1.233$

$r_1 = .371\,\Omega$ $\quad x_1 = 42.3\,\Omega$ $\quad g_m = .000155$ $\quad b_m = .00392$ $\quad I_0 = 0.83$ AMP $\quad \cos\theta_0 = 0.295$ $\quad \sin\theta_0 = 0.955$

$r_2 = .188\,\Omega$ $\quad x_2 = .899\,\Omega$

$r_L = 55\,\Omega$ $\quad x_L = 41.8\,\Omega$ $\quad Z_L = 697\,\Omega$ $\quad Z_L =$ $\quad Z_L =$ $\quad Z_L =$

1	$a_F a_I r_2 + j a_I x_2$	$195 + j 6.38$					
2	$a_F a_I r_L + j a_I a_F x_L$	$577.5 + j 434.2$					
3	① + ② = $r'_{2L} + j x_{2L}$	$597 + j 498$					
	Z'_{2L}	Z'_{2L} $\quad 779 \quad 604400$					
4	$g_{2L} - j b_{2L} = 1/③$	$0.000988 - j 0.000824$					
5	$g_m - j b_m$	$0.000155 - j 0.00392$					
6	④ + ⑤ = $g_{2LM} - j b_{2LM}$	$0.001143 - j 0.004744$					
	Y_{2LM}	Y_{2LM} $\quad 0.00488 \quad 0.00002304$					
7	$r_{2LM} + j x_{2LM} = 1/⑥$	$48 + j 199.2$					
8	$r_1 + j x_1$	$.371 + j 42.3$					
9	$r_{12} + j x_{12}$ = ⑦ + ⑧	$85.1 + j 241.5$					
	Z_{12} $\cos\theta_1$ $\sin\theta_1$	$256\angle 0.332 \quad 0.944$					
10	$I_1 = E_1/Z_{12}$	$\frac{254}{256} = 0.99$ AMP					
11	$E_1 \cos\theta_1 + j E_1 \sin\theta_1$	$84.4 + j 240$					
12	$I_1 r_1 + j I_1 x_1$	$.367 + j .418$					
13	⑪ − ⑨ = Δ + jΔ	$477 + j 198.2$					
	EMF_{1FL} - from ⑫ or I_1/Y_{2LM}	204.5 VOLTS					
14	$I_2 = EMF_{1FL} / a_I Z'_{2L}$	$\frac{204.5}{0.1187(779)} = 2.24$					
15	$E_{2FL} = I_2 Z_L$	154 VOLTS					
16	$P_2 = m_2 E_2 I_2$	1020 VA					
17	$REG = E_{20}/E_{2FL}$	$171.6/154 = 1.12$					
18	$E_1 \cos\theta_0 + j E_1 \sin\theta_0$	$749 + j 242.5$	22	P_{2W}	− 816 WATTS	−	
19	$I_0 r_1 + j I_0 x_1$	$30.8 + j 35.1$	23	$3 I_2^2 r_2$	− 28 "	−	
20	⑱ − ⑲ = Δ + jΔ	$441 + j 207.4$	24	$CORE_2$	− 140 "	−	
	EMF_{10} - from ⑳ or I_0/Y_m	211.5 VOLTS	25	$3 I_1^2 r_1$	− 109 "	−	
21	$E_{20} = EMF_{10}/a_E$	171.6 VOLTS	26	$CORE_1$	− 30 "	−	
			27	F.F. & W	− 80 "	−	
			28	Σ ㉒ TO ㉗	− 1203 WATTS	−	
			29	$P_{1W} = 3 I_1 E_1 \cos\theta_0$	+ 251 "		
			30	P_m	− 952 WATTS		

Figure 7. Design sheet

28.) In line 29, the primary watts are introduced with a positive sign, if absorbed, or a negative sign, if delivered. Line 30 gives the mechanical power as the algebraic sum of the values in lines 28 and 29. If the minus sign appears in line 30, the shaft of the converter has to be driven by a motor; if a plus sign appears, the shaft delivers mechanical energy which must be absorbed by a generator.

Appendix

Derivation of the Current Locus of a Frequency Converter, Loaded With a Constant Secondary Power Factor, by Changing the Load Impedance From Zero to Infinity

With reference to the equivalent circuit, Figure 5, the total secondary impedance is given by:

$$Z_{2L}' = Z_2' + \lambda Z_L' \tag{9}$$

λ is a real parameter which, in its most general form, may vary between $+\infty$ and $-\infty$

$$Y_{3m} = Y_m + 1/Z_{2L}' = \frac{(Y_m Z_2' + 1) + \lambda Y_m Z_L'}{Z_2' + \lambda Z_L'} \tag{10}$$

$$Z_{12} = Z_1 + 1/Y_{3m} = \frac{(Y_m Z_1 Z_2' + Z_1 + Z_2') + \lambda(Y_m Z_1 Z_L' + Z_L')}{(Y_m Z_2' + 1) + \lambda Y_m Z_L'} \tag{11}$$

$$I_1 = \frac{E_1}{Z_{12}} = E_1 \frac{(Y_m Z_2' + 1) + \lambda Y_m Z_L'}{(Y_m Z_1 Z_2' + Z_1 + Z_2') + \lambda(Y_m Z_1 Z_L' + Z_L')} \tag{12}$$

Equation 12 is of the form:

$$P = \frac{A + \lambda B}{C + \lambda D}$$

which is the equation of a circle in parametric form. The determination of the coordinates of the center point, and the radius, can be found in standard texts. For the considered load range, in which power is consumed by the load in the secondary circuit, the primary current follows the arc of the circle between the two points corresponding to $\lambda = 0$, and $\lambda = +\infty$.

List of Symbols*

r_1, r_2 = resistance in primary and secondary
x_1, x_2 = reactance in primary and secondary at f_1
g_m = conductance
b_m = susceptance
Y_m = admittance
} of magnetizing branch
r_L = resistance value of load
X_L = reactance value of load at f_2
Z_L = impedance value of load at f_2
Z_{2L}' = total secondary impedance referred to primary
Z_{12} = total impedance of the equivalent circuit
E_1, E_2 = phase voltages in primary and secondary
E_{20}, E_2 = secondary voltage at no load and at full load
EMF_{10}, EMF_{1FL} = primary electromotive force at no load and at full load
I_0, I_1 = primary current at no load and at full load
I_2 = secondary current
m_1, m_2 = number of phases in primary and secondary
N_1, N_2 = turns in series, per phase (primary and secondary)
K_{dp1}, K_{dp2} = combined distribution and pitch factor (primary and secondary)
a_E, a_F, a_I, a_Z = reduction factors for voltage, frequency, current, and impedance
n, n_s = shaft speed and synchronous speed in rpm
P_1, P_{1W} = primary power in volt-amperes or watts
P_2, P_{2W} = secondary power in volt-amperes or watts
P_m = mechanical power in watts
P = number of poles
$\cos\theta_0, \cos\theta_1$ = primary power factor at no load and at full load
$\cos\theta_2$ = secondary power factor
λ = real parameter

* See also design sheet, Figure 7.

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No Discussion

An Improved Design of Low-Voltage Panel-Mounted Air Circuit Breaker

LEON H. SPEROW
MEMBER AIEE

JOHN A. FAVRE
NONMEMBER AIEE

THE BASIC DESIGN of the general purpose low-voltage panel-mounted air circuit breaker has remained substantially unchanged for more than ten years. While the need for improvement has been recognized by the industry, the pressure of the war years has delayed this undertaking. The extraordinary demands of the armed services were usually outside the more familiar power and industrial requirements, and thus forced research and development that would ordinarily have spread over a longer interval.

Figure 1. 600-ampere manually operated, enclosed air circuit breaker

This work in a large measure was carried on by men who could recognize the value that much of their work would have in the commercial field. After the end of hostilities, no time was lost in starting a program of modernization to recover the losses from obsolescence of the war years. The principal accent was, of course, on performance, with reduced size and appearance running a close second. Appearance, or as it is better known, "the new look" could not be neglected either. This paper will describe in detail the features offered in the circuit breaker design which this program produced.

The New Circuit Breaker

Figures 1 and 2 show the new 225- and 600-ampere frame size air circuit breaker.

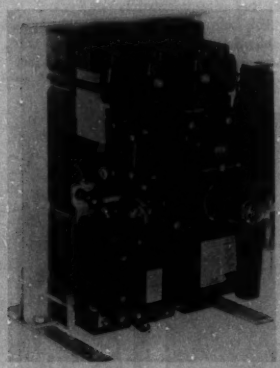

Figure 2. 225-ampere electrically operated air circuit breaker, for stationary switchboard mounting

The principal advantages offered in this design over older ones are specifically:

1. An improved arc chute of reduced size.
2. Long-lived contacts with momentary ratings equal to interrupting ratings.
3. Flexible and rugged overcurrent devices capable of necessary precision timing for selective tripping systems, but equally suitable for simple overload protection.
4. 30 per cent reduction in size—four circuit breakers in the space now occupied by three.
5. Longer life and lower maintenance from improved design.

Contacts and Interrupter

Figure 3 shows a section through one contact finger and part of the arc quencher construction. Two or more of these contact fingers are used for each pole and are separated by perforated phospho-asbestos arc barriers as shown. This design makes it possible to split the arc and quench three or four small arcs in a smaller space than an equivalent single arc requires. The holes in the barriers are of a size and number found to be necessary for arc transfer so that the interrupting job is equally divided. This is necessary to prevent the arc from concentrating in one slot or compartment. The contacts are faced with a sintered silver bearing, arc-resistant material admirably suited to the dual requirements that they efficiently conduct current and still resist the errosion associated with arc interruption. Their efficiency is attested by the fact that they have a momentary rating equal to their interrupting rating and that their resistance after arc interruptions of considerable magnitude shows little change. Much experimenting was done until this result was achieved, as the arc resistant components of many tip facings that were tested are inclined to collect at the surface

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LEON H. SPEROW and JOHN A. FAVRE are both with the air circuit breaker division of the General Electric Company, Philadelphia, Pa.

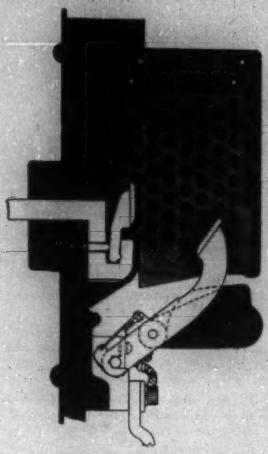


Figure 3. Contacts and interrupter

with attendant increase in resistance and heating. Inspection of Figure 3 will show the absence of heavy braids. The front or movable contacts are hinged on a close fitting preloaded silver plated pin, while the back or fixed contacts use a line contact pivot. It will also be observed that the fixed contacts with their supports form a reverse loop which increases the contact pressure under the action of high current. This feature is of considerable value as it permits lighter contact pressure, than is permissible without such a loop, which in turn reduces the wear on the contact and mechanism. Two things to avoid which may spoil an otherwise good contact design are bounce on closing and slow separation on opening. Bounce may be caused by any one of several factors, but must be suppressed. Oscillograms of these contacts show them to be quite free of this troublesome action.

Investigations made on contact wear indicate that life is increased by as much as three or four times if a contact opening of at least 40 mils is achieved in less than one millisecond when arcing is present. Adequate provision for wipe makes comparatively high opening speed easy in this design, as initial acceleration occurs before the contacts part.

The interrupter and contact structure has been thoroughly tested over the entire range of currents to establish the following interrupting ratings:

225-Ampere Frame Size	
600 Volts Alternating Current	15,000 Amperes
250 Volts Direct Current	15,000 Amperes

600-Ampere Frame Size	
600 Volts Alternating Current	25,000 Amperes
250 Volts Direct Current	25,000 Amperes

Figure 4 shows a typical oscillogram of a 3-phase 60-cycle, a-c interruption test made on the 600 ampere frame size circuit breaker. The average current of the three phases interrupted is 31,667 amperes at 590 volts and the total interrupting time is approximately one cycle.

This contact structure has proved capable of carrying 15,000 and 25,000 amperes for one second on the 225 and 600 ampere frame sizes respectively.

Overcurrent Tripping Devices

The familiar dual magnetic tripping devices relying on the adhesion of two mating disks in oil have served industry for many years. For most commercial applications in the past, they gave satisfactory service, but fundamental limitations became serious with the advent of selective tripping when more precise and reliable timing became a must. There are many varieties of timing devices, but they are all based on a very few fundamental principles as old as recorded history with the possible exception of some electronic methods. In approaching the design of the timing devices, there was no lack of material to choose from. The problem faced by the designer was more one of discriminating choice than the necessity of outright pioneering.

The new series overcurrent tripping device shown in Figure 5 is a three element device somewhat similar to the one used for several years on Navy circuit breakers, Figure 6. The new device has two armatures associated with a common magnetic circuit and series coil. One armature operates the long time and instantaneous elements, Figure 5*B*, the other, the short time element, Figure 5*A*.

The long time element is of the positive displacement type using silicone oil to minimize the effect of temperature change and avoid sludging. The unit is hermetically sealed to prevent contamination. The piston carries a fixed orifice of a size suitable for the time-current characteristic desired.

The top left portion of Figure 7, above *C*, is representative of the characteristic and is shown as a solid band rather than a single line curve. The top limit represents the maximum time including variations of the device and the maximum in-

Figure 4. Typical oscillogram of a 60-cycle 3-phase interrupting test

A—Line voltage
B—Arc voltage, left pole
C—Current, left pole
D—Arc voltage, center pole
E—Current, center pole
F—Arc voltage, right pole
G—Current, right pole
H—Extra vibrator

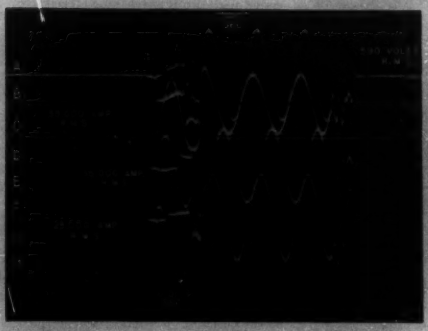
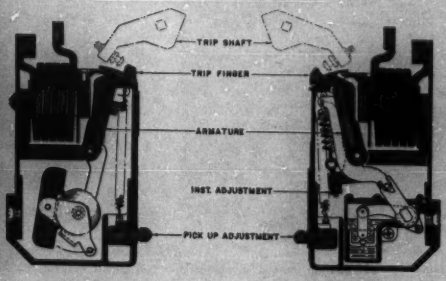


Figure 5. A 2-armature 3-element overcurrent tripping device



terrupting time of the circuit breaker. The bottom limerit presents the minimum tripping time including variations. Below the band is a dotted curve of reset time defined as the maximum time that the device will carry a given value of current, then reset when the current is reduced to a given percentage (80 per cent for the long-time curves) of the pickup setting. Standard pickup settings are 80 to 160 per cent of the coil rating and may be set by adjusting the scale plate on the front of the device. Setting this scale plate varies the restraining force on the armature.

The armature for the long-time element is connected to the piston by means of a heavy spring, which provides the instantaneous feature of the device. This permits the instantaneous element to be set in the factory to any required value up to 100 times coil rating. However, in a few cases, the momentary rating of the circuit breaker or the amount of delay to be employed may impose limitations on this setting.

The short time armature operates an escapement type of timing mechanism, Figure 5*A*, which produces an inverse time current characteristic. The escapement wheel is made with teeth only on part of its periphery, thus, the delay is set by varying the position in the stroke of the armature at which the escapement wheel runs free of the pallets.

Since the function of the short time element is to give short delays in the fault current range, pickup settings are from five to ten times coil rating. The upper and lower limit of the time current characteristics are similar to the long-time characteristics except that the reset time is based on the fault current reducing to 20 per cent of the pickup setting. The middle portion of the solid band in Figure 7, between *A* and *B*, is a typical time-current characteristic.

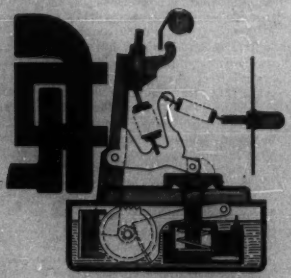


Figure 6. A single-armature 3-element overcurrent tripping device

The three elements of this overcurrent tripping device have been designed to be combined in any way desired. Standard combinations are:

Instantaneous, high set and nonadjustable
Instantaneous, low set and adjustable
Long time and instantaneous
Short time and instantaneous
Long time, short time and instantaneous

A combined time-current characteristic of a device having all three elements is shown by the solid band in Figure 7 and for one having only long time and instantaneous by the shaded band. In the case of the solid band, the adjoining portions of the different characteristics are produced by separate armatures, thus the sharp intersections at *A* and *B*. However, for the shaded band, both portions of the characteristics are produced by the same armature and their intersection is rounded out as at *C*. Thus for a single armature device (Figure 6) or one in which the timing elements do not function independent of one another, the intersections will be rounded as of *C*.

Reduction in Size

The advantages of reduced size, as shown by Figures 1 and 2 were achieved, in part, by careful design calculated to make the most of smaller elements used as auxiliaries. The operating solenoid was designed carefully to produce the maximum performance from iron and copper. A new control was designed which is inherently antipump and so on down the line with the result that space reduction was achieved with no loss of any function usually associated with circuit breaker control. The reduction in size has not only been reflected in the new circuit breakers themselves, but also in the switchgear equipments using them. For example, the drawout switchgear equipment in Figure 8 has 600-ampere circuit breakers stacked four high in each unit. Previous designs would accommodate only three per unit. The new drawout equipment has each circuit breaker assembled into a steel truck fitted with free running rollers and rack-out mechanism which make the removable element easy to insert or withdraw.

Sturdy primary disconnecting contacts of a design capable of handling momentary current in excess of the circuit breaker interrupting rating assure dependable cascade operation. Secondary disconnects have been reduced in size so that as many as 28 points per circuit breaker are available for connecting auxiliary and control devices.

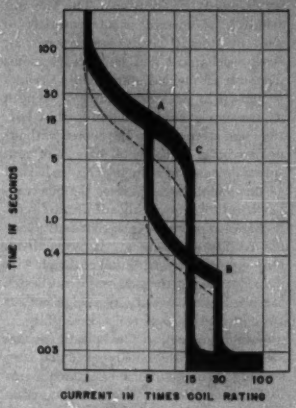


Figure 7. Typical time-current characteristic of the 2-armature 3-element overcurrent tripping device

Maintenance

Longer life and lower maintenance are also products of good design. For many years ebony asbestos, masonite die stock, laminated phenolics and similar materials have been used for air circuit breaker mounting bases. While they showed an improvement over the old slate panels, a still better base was necessary. The advantages of a composite aluminum and steel base supporting separate molded base units become more obvious as this

Figure 8. Drawout switchgear equipment using the new 225- and 600-ampere air circuit breakers

arrangement is discussed. The low-unit strength of former panel materials of necessity made them cumbersome for both mechanical and electrical reasons. The use of molded components permits reduction in size and improved strength as creepage is readily maintained by barriers and strength can be secured without useless bulk. The metal base is of considerable value in reducing the fire hazard as this metal base separates the bus compartment of a switchboard from circuit breaker compartments so that faults cannot readily communicate between them. One important contribution of the plastics industry during the war was an asbestos rag-filled phenolic with very high impact strength and good arc resistance. Liberal use of this and cotton-filled plastic is characteristic of this circuit breaker and is in keeping with the plastic era which seems to be a mark of the times. The versatility and usefulness of plastics for both mechanical and electrical reasons is well known to all, but in passing, it might be pointed out that original finish and appearance lasts for the life of the device.

This circuit breaker features a trip shaft carried in two sealed antifriction bearings which should never require attention because of sticking. The latch itself bears on a similar bearing. The operating linkage is fabricated from alloy steel and bronze and is lubricated with an extreme temperature lubricant of high staying power. The contacts are operated through plastic links so that we have a dead front circuit breaker with overcurrent tripping devices that can be adjusted if necessary while energized. Operating solenoid, trip units, and contacts are readily accessible, or replaceable from the front. Most of the components are the same in both circuit breakers so that spare parts inventories are kept to a minimum. The maintenance problem is simplified further because nearly all of the accessories are designed so that they may be precalibrated as a complete unit, then assembled to the circuit breaker without further test. The accessories are designed for removal from the front of the circuit breaker thus simplifying the maintenance of those circuit breakers which cannot be removed to a bench.

Summary

Close co-operation with the Navy during the war in working out solutions for rapid improvement of pre-war standards of protection and control of low-voltage distribution circuits, gave the group that developed these circuit breakers an enviable background of experience to fit them for the program just discussed. The results of their efforts have been outlined and can be summarized by saying that industry is offered a new circuit breaker in which high inrush and momentary currents are easily handled, with an increased contact life.

Accurate and proved timing elements have been provided giving a variety of desired time-current characteristics.

A wider application on motor and other repetitive duty high inrush current circuits is assured by longer contact and mechanism life and accurate delayed tripping obtained with the fluid displacement timing elements.

Dependable selective operation can be obtained without the additional expense of current transformers and relays.

Contacts and coils which will stand the high momentary currents, while tripping is delayed with an accurate mechanical timing device, make possible dependable selective operation, so unquestionably proved on the Navy's major ships.

Discussion

M. E. Horn (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The authors are to be congratulated on their presentation of the performance requirements of low-voltage air circuit breakers. It may not be known generally that the admirable degree to which circuit breaker designs were produced to meet armed service requirements was made possible by the pooling of ideas and research information on an industry-wide basis. It is believed that the authors will readily agree that even a majority of the contributions to the art of circuit breaker design in the war period could not be attributed to any one manufacturer. Consequently, new commercial designs now reaching the market are likely to show marked similarity in improved operating characteristics.

With reference to statements in the paper regarding the reduction in space required for mounting the new circuit breaker and Figure 8 showing four unit high drawout gear, additional amplifying statements by the authors may be in order. In view of the fact that at least one manufacturer has been offering drawout or fixed mounted gear of this same rating in four unit sections 90 inches high for the past eight years or longer, the question naturally arises as to the height of the switchboard sections shown in Figure 8. In this presentation, the authors have exhibited considerable knowledge of the problems related to contact design, and their frank discussion leaves the impression that the new contacts will have an exceptionally long operating life. It is further noted that Figure 3 does not show the contact tips to be renewable. It is assumed, therefore, that to renew the moving contact considerable disassembly of the circuit breaker is involved. Should it also be assumed that contact life approached the life of other circuit breaker parts and that contact renewal is contemplated only as a major overhauling operation? The matter would be considerably clarified by a statement by the author concerning the expected contact life, particularly on circuits involving considerable stored energy, such as may be encountered on 250 volt d-c circuits to which the circuit breaker is applicable or on a-c motor starting applications.

The series overcurrent tripping device is shown with considerable detail by Figure 5. It appears that the linkage operating the long time delay plunger enters the fluid compartment through a bearing shaft arrangement. A screwed-on cover is also indicated for the fluid compartment. However, the statement is noted: "The unit is hermetically sealed to prevent contamination." Inasmuch as Webster defines the term "hermetically sealed" as "sealed by fusion" the question arises as to how this seal by fusion could actually be performed with functional parts arranged as per Figure 5.

Leon H. Sperow and **John A. Favre**: The performance specifications for the Navy circuit breakers on which this new design is based were determined by requirements of the armed services as pointed out by the authors. Undoubtedly many of the items of these specifications were the result of suggestions by various manufacturers.

However, the devices used on this new line of circuit breakers are ones which were developed by the authors' company.

The statements in the paper regarding the size of the new circuit breaker were directed primarily at previous designs with which the authors have had direct experience. In the case of the earlier designs, only the 225-ampere frame-size circuit breaker could be supplied in four-unit drawout mounted gear of 90-inch height. With the new design both the 225- and the 600-ampere frame size can be furnished in four-unit sections, 90 inches high and 20 inches wide.

Contact life on both interrupting and closing duty has been greatly increased over that of previous designs. Tests on high inrush currents such as encountered on a-c motor-starting applications show, for example, that the contacts of the 600-ampere frame size circuit breaker will last more than 2,500 operations at 4,000 amperes.

Reference to Figure 3 will show that both the movable and stationary contact fingers are relatively small parts. Their design is such that the complete fingers may be economically replaced in the field whenever it becomes necessary. The replacement can be readily achieved since the subassemblies have been designed for easy removal from the front of the circuit breaker.

The authors used the term "hermetically sealed" in its more common sense, that is, "completely sealed by any means." The top of the cylinder is sealed by a cemented and gasketed screwed-on cover. The bearing and shaft arrangement is completely sealed by a synthetic rubber sleeve which is cemented and clamped around the shaft and bearing.

A New Isolated-Phase Metal-Enclosed Bus Designed for High-Capacity Generating Stations

B. W. WYMAN
MEMBER AIEE

R. B. SHORES
ASSOCIATE AIEE

THE design of bus runs for high-capacity generating stations has always received considerable attention in order to insure freedom from faults of any type, whether due to insulation deterioration, insulation contamination, mechanical failures, or other causes. With greater concentrations of generating capacity in single stations, short-circuit currents have steadily increased and the damage resulting from bus faults has steadily become more serious, so that constantly greater reliability has been sought in station bus design. In addition, many power systems are now operating with very low reserve capacity so that bus faults which might cause the loss of any station or part of a station, even for short periods, must be guarded against. This paper describes the development of a metal-enclosed isolated-phase bus for generating stations, which combines a high degree of reliability with immediate accessibility for inspection or maintenance.

Background

Early designers of station bus realized that the short-circuit current in phase-to-ground faults could be limited by neutral impedance, whereas phase-to-phase faults could not, so their attention was directed toward station layouts which would prevent phase-to-ground faults from developing into phase-to-phase faults. Some of the first attempts to eliminate phase-to-phase faults resulted in concrete barriers being constructed between phases. This construction, while an improvement in reducing the frequency of phase-to-phase faults, was difficult to keep free from contamination or rodents, and some operators felt it actually increased the frequency of phase-to-ground faults. It also left much to be desired both in safety to personnel and in ease in extending or relocating. Even greater freedom from phase-to-phase faults was attained in some cases by locating each phase on a separate floor. As each circuit breaker phase then had to be located on a separate floor, extremely awkward and bulky station layouts resulted. This idea was abandoned in favor of metal-enclosed bus runs with each phase conductor completely insulated by Herkolite or compound, but with all in a common metal enclosure. The compactness of this type of bus together with its ease of installation and safety to personnel has ideally suited it to all metal enclosed switchgear in which fault magnitudes do not exceed 500,000 kva. Compound supports are normally used in this construction to support the insulated conductors. The upper section of Figure 1*a* shows a cross section of this type of nonsegregated phase which has become standard for many of the smaller industrial plants and generating stations.

For large generating stations, on the other hand, where short circuit magnitudes may exceed 500,000 kva, and where load currents run into several thousand amperes, it has normally been necessary to resort to tubular or channel busses, both to carry the high load currents and to withstand the magnetic forces encountered during short circuit. As wrapped or pressed-on insulation for this type of bus is economically impractical, it has been customary to use only air insulation and to support the channel conductors on porcelain insulators. To prevent phase-to-phase faults with this construction, the phase conductors were then spaced far enough apart so that metal barriers could be located between phases as shown schematically in Figure 1*b*. This

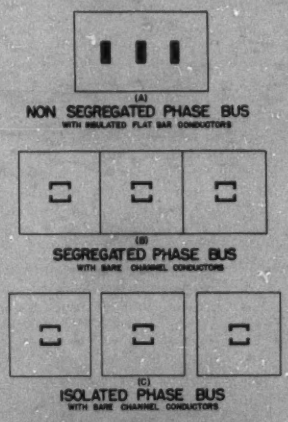


Figure 1. Schematic comparison between different types of metal enclosed bus

A. Nonsegregated phase in common metal enclosure with flat bar conductors insulated by compound

B. Segregated phase bus with all three channel conductors insulated by air and in common metal enclosures with metal barriers between phases to prevent phase-to-ground faults from developing into phase-to-phase faults

C. Isolated phase bus with each channel conductor insulated by air with each conductor in its own metal enclosure, thereby providing two metal barriers between phases for greatest security against phase-to-ground faults developing into phase-to-phase faults

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B. W. Wyman and R. B. Shores are both with the General Electric Company, Philadelphia, Pa.

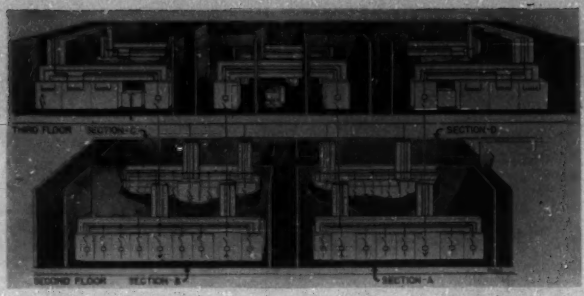


Figure 2. Actual station circuit breaker layout using earlier design of isolated phase bus up to 5,000 ampere ratings and high capacity station type air blast circuit breakers rated up to 2,500,000 kva

segregated phase construction is in use in a large percentage of the higher capacity generating stations throughout the country.

The reliability of the metal barrier between phases in eliminating phase-to-phase faults has been demonstrated by the thousands of circuit years operating experience without outage. Most of these segregated phase bus installations have been ventilated by louvers so that the minimum cross-section conductors could be used without overheating. Because of the extreme importance of service continuity on all equipment at the present time connected to these busses, many operating engineers are now specifying dust-tight enclosures without louvers to guard against dust depositing on insulator surfaces.

It has been realized for years, however, that one of the weaker points of the segregated phase construction was the possibility of the phase-to-ground arc burning through one of the metal interphase barriers, and thereby developing into a phase-to-phase fault, or double phase-to-ground fault. Although the possibility of the barrier burning through is remote if the fault arc is interrupted quickly, it does become more likely if the fault current is left on an appreciable length of time, or if the current magnitude is high. The possibility of an arc burning through such a metal barrier is greater in the bus sections connected directly to the generator terminals ahead of the first circuit breakers because of the time interval required for the fault current to decay to zero.

Tests have shown that protection can be obtained against such a burn-through if each phase conductor is enclosed in its own nonventilated metal enclosure and the three enclosures are then separated by air spaces. With this arrangement, even though an arc burns through the side of one enclosure, ionized gases cannot get into adjacent enclosures. Several different designs of bus with each phase bus conductor in its own enclosure have been installed in the past several years in which the basic idea of air separation between the metal enclosures has been proved sound. Figure 1*c* shows schematically this isolated phase construction.

Figure 2 shows the layout of a large modern generating station, and Figure 3 is a view inside the station showing one design of isolated phase bus connected to some 5,000-amperes 2,500,000-kva station-type air blast circuit breakers.

After reviewing existing designs of busses it was decided to undertake the development of a completely new bus type which would afford a high degree of reliability with immediate accessibility for inspection and maintenance, and also provide maximum ease of installation by designing for complete factory assembly to reduce the amount of work necessary in the field. By questioning operating engineers, construction engineers, and consulting engineers, a set of specifications was established to guide the development of the new design.

Requirements for New Design

The following set of specifications was set up as requirements for the new design:

1. Ultimate design must permit shipment of complete factory assembled sections to reduce field installation time to a minimum.

2. Designs must permit maximum accessibility for inspection or maintenance so that

Figure 3. Earlier type 5,000-ampere isolated-phase bus being installed in station layout shown by Figure 2. The 2,500,000 kva air blast circuit breaker is in the background

Figure 4. Single phase section of new type isolated phase bus to provide greater reliability for high capacity generating stations

Cover in closed position showing unusually clean appearance due to square enclosure without protruding bolts or screwheads. This section would constitute an individual phase shipping unit for easy installation in field

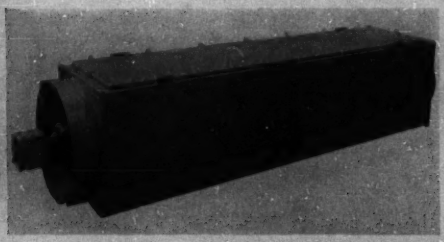


Figure 5. Single-phase section of new type isolated-phase bus with cover open showing ready accessibility to bus for inspection or replacement of any part

This section is rated 4,000 amperes continuous current with momentary short-circuit current rating of 130,000 amperes

any insulator, or bus section could be inspected, or even removed and replaced, in the shortest possible time. As some busses will be installed in hazardous locations (such as on scaffolding or suspended from ceilings) unbolting or handling of heavy, loose covers should be avoided by hinging them.

3. The number of porcelain insulators should be kept to a minimum consistent with strength requirements in order to reduce the number of creepage surfaces, as each unnecessary creepage surface constitutes a potential flashover point.

4. Angular and longitudinal flexibility should be designed into the bus and enclosure junctions so that no high accuracy alignment is necessary in the field which will increase the cost of installation.

5. Longitudinal flexibility should be designed into the bus junctions to provide for thermal expansion and contraction of long runs without introducing strains in either bus supports or porcelain insulators.

6. The metal enclosures should be dust-tight to prevent dust, moisture or other contamination from entering the enclosure, which might reduce the flashover strength over insulator surfaces. This would eliminate the necessity of porcelain cleaning which has always been necessary on ventilated enclosures. In addition, it would not permit the entrance of ionized gas from an arc into an adjacent phase even if that arc burned through its own enclosure.

7. The metal enclosures should be weather-tight so that they can be installed either indoors or outdoors without special construction or additional installation precautions.

8. Temperature rise limitations, 60-cycle high-potential withstand tests, and basic-

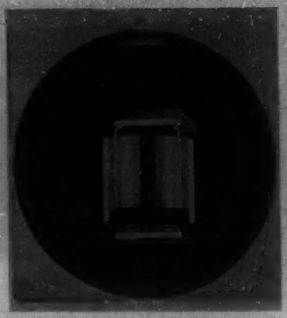


Figure 6. End view of isolated-phase bus showing manner in which 4,000 amperes channel conductor is supported by standard porcelain insulators, with provision in conductor support for thermal expansion or contraction of conductor by slide fit

Porcelains are supported on heavy removable brackets which in turn are bolted to supporting pads. These pads are rigidly fastened through the enclosure to the H-beam cross member

impulse withstand tests must be in accordance with AIEE Standards for station-type switchgear.

9. A generous margin of mechanical strength should be designed into the bus supports to withstand the maximum magnetic forces associated with the rated momentary short-circuit currents.

10. Metal enclosures should be constructed of nonmagnetic material to eliminate magnetic heating and thereby permit minimum cross-section conductors.

11. Seal-off bushings should be provided at appropriate locations to act as arc and gas barriers in order to prevent the possibility of a phase-to-ground arc within an enclosure running along the entire bus length.

Design of New Isolated Phase Bus

Figure 4 shows a 4,000-ampere single-phase section of the new isolated phase bus with the cover closed. Figure 5 is this same section with the cover opened, showing the complete accessibility to the bus which is possible when the cover is open. The design of the enclosure consists essentially of a single aluminum sheet shaped to form three of the sides, while the fourth side consists of a gasketed cover. The square configuration with no screws and bolts projecting through the surfaces results in an exceedingly clean attractive appearance. The cover is hinged on either side so that it may be swung open to inspect or replace any part after merely loosening clamping bolts. The channel bus is supported on standard porcelain insulators which are normally carried in stock in all power stations at the present time. Silver surfaced semiflexible copper connections are used to connect the end of each channel section to the next section, thereby providing angular flexibility as well as providing for thermal expansion and contraction of the bus. The porcelain insulators supporting the bus are in turn mounted on removable heavy steel supporting brackets as shown in Figure 6. These removable brackets are in turn bolted to two sturdy feet. Any insulator may, therefore, be removed or replaced at

Figure 8. Close-up view showing split banding ring which clamps over gasketed circular flanges of adjacent end plates

These banding rings between housing sections provide angular and longitudinal tolerance for ease of installation. These bands also permit standardization of housing lengths by varying the width of the banding rings and flexible copper connections to suit individual installations

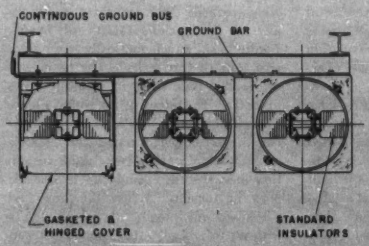


Figure 7. Three-phase assembly of the new isolated-phase bus

This unit consists of three individual phase housings suspended from two H-beam cross members which constitutes a 3-phase shipping section

Figure 9. Two 8-foot development sections assembled for 110-kv impulse withstand test with covers removed from center phase to show bus

Lower section with final type hinged cover to permit easy accessibility

Figure 10. A 4,000-ampere development sample bus section set up in high-capacity short-circuit testing station for short-circuit tests up to 200,000 amperes

will. The steel supporting feet are bolted solidly through the housing to the main *H* beam which carries all three phases. The details of this construction are shown in Figure 7.

Each end of the housing fits into the gasketed groove of an aluminum end plate. A circular flange with an insulating gasket around its circumference projects from these end plates. Simple split aluminum banding rings are then used to span the distance between the end plates of adjacent sections. When tightened up, these aluminum banding rings clamp down on the gaskets to exclude any possibility of dust entrance, as may be seen in Figure 8. These gaskets provide insulation between each section of the housing to eliminate the flow of circulating currents. The length of these banding rings can be varied at will so that most bus sections in a given length of bus run can be a standard uniform length which is ideal for both manufacture and installation. A heavy continuous longitudinal ground bus extends the full length of the bus run and is connected to each section. Insulation spacers are provided between metal housings and H-beam cross members as necessary to break up stray circulating current paths which could cause overheating.

All gaskets have been selected to make the sections both dust-tight and weather-tight so that the same construction is used for either indoor or outdoor service. In order to test the tightness of the unit against dust, a section of bus was located in a sandblast enclosure for some time where the dust was so dense that it was impossible to see more than a few feet. To test its weather tightness, a high-pressure spray from a nozzle was directed on the gasketed joints demonstrating the ability of the gasketed enclosure to keep moisture out.

Tests on Isolated Phase Bus

After successfully completing both 60-cycle high-potential test and impulse withstand tests, heat runs on the various ampere ratings were made to be sure that no ratings exceeded more than 45 degrees centigrade rise on the conductor and 30 degrees centigrade rise on the enclosure. An extensive series of short-circuit tests were then made to determine whether the unit would withstand the magnetic forces associated with the maximum momentary current rating of the bus. Figure 9 shows a 2-section development test unit being prepared for 110-kv impulse withstand tests. Both center phase sections have their covers removed to show the channel bus and supporting insulators.

Figure 10 shows these test sections connected for test and located on the test car inside a test cell in the high-current short-circuit laboratory. A total of 69 short-circuit tests were made at varying currents and with various types of faults (both phase-to-ground and phase-to-phase, up to 200,000 amperes) which exceeds the momentary rating for any of the various ratings of busses. Figure 11 shows schematically representative tests of this series. The porcelains are loaded differently in different types of faults. In test 39, at the top of Figure 11, is a phase-to-phase fault test of 200,000 amperes, which repels the 2-phase conductors, places the two outside insulators in compression and the two inside ones in tension. When the bus had withstood all possible types of faults without damage, the porcelains on one side of the bus were removed and the tests repeated. Tests 43, 51, and 61 are examples of these tests. Although this placed a double load on the remaining insulators, close inspection still revealed no damage to any parts. These latter tests, with half the insulators removed, probably represented much more severe conditions than would ever be required in service. However, it was felt that with the high degree of reliability required of this type bus in service, this extra margin was desirable rather than using lighter porcelains which probably could have been done.

Installation of Bus

The complete assembly of the bus in the factory reduces installation time to a minimum. The bus can be shipped either as individual phase sections or 3-phase sections, depending upon which happens to be the most convenient. As the entire enclosure is made of aluminum, the weight is kept to a minimum for easy handling. The flexibility between the various sections of both bus and enclosure eliminates the necessity for any accurate alignment or location of one section with respect to another, which greatly reduces the cost of installing this type of bus over previous designs. As blank end plates or banding ring end plates are interchangeable, later extensions, side tap-off additions, or rearrangements can be made in a minimum of time. Figure 12 shows an actual station plan using this new type of isolated phase bus in a unit generation steam station, running from the generator terminal inside the plant to the three single-phase step-up transformers outside the building, and to one 3-phase station service transformer.

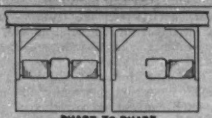
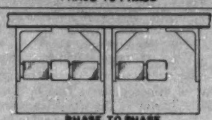
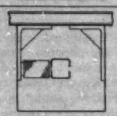
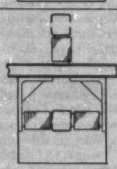
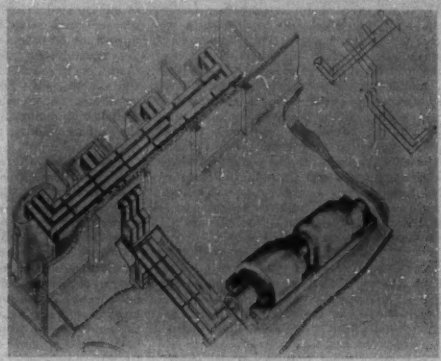
TEST NO.	SET UP	AMPERES (RMS. FULLY OFFSET)
39	PHASE TO PHASE	200,000
43	 PHASE TO PHASE	140,000
51	 PHASE TO PHASE	191,000
56	PHASE TO GROUND (HOUSING RETURN)	200,000
61	 PHASE TO GROUND (HOUSING RETURN)	174,000
69	 PHASE TO PHASE	200,000

Figure 11 (left). Typical momentary short-circuit current tests on a sample section of the new isolated-phase bus run rated 15-kv 4,000-amperes continuous with momentary rating of 130,000 amperes

A portion of the 69 tests conducted on this sample section were made with the deliberate removal of half the porcelain insulators in order to prove the design of the individual supporting brackets. At the conclusion of this set of tests, no damage had been incurred to any parts of the bus run

Figure 12. Typical station layout of unit generation scheme showing new type isolated-phase bus connecting from generator to disconnecting switches to three out-door single-phase step-up power transformers and a 3-phase station service transformer

The same construction is used for both indoor and outdoor portions of bus. Potential transformers and surge protection equipment are located inside station connected by a tap-off from the main bus



The connections between the conductors of a metal enclosed bus and the terminal bushings of a generator or power transformer are made by means of flexible braid to prevent any misalignment or thermal expansion strain being placed on such bushings. It is usually possible to maintain a high insulation level in the housing for transformer terminals, but the close spacing of some generator terminals occasionally makes it difficult to retain the full 110-kv impulse withstand when it is necessary to carry the isolation of phases to the generator terminals.

Provision is made for spanning the joint between the generator foundation and the building proper with a flexible connection in both the housing and the conductor.

Conclusions

The isolated phase bus design described in this paper fulfills all of the stated requirements for high-capacity generating station bus. It is proving very flexible in application, and its installation is relatively simple. Tests have proved that it will meet its rating with a generous design margin. It is believed that this development has resulted in a bus for high-capacity generating stations and large industrial plants which will afford a high degree of reliability with immediate accessibility for inspection and maintenance.

Discussion

P. R. Pierson (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The authors have prepared a very interesting presentation of isolated-phase metal enclosed bus for high-capacity generating stations. A new design is offered which has many desirable features for those applications having exceptionally heavy duty requirements. The subject has been carefully studied and a good background of test data has been provided.

The statement that most of the segregated phase bus installations have been ventilated by louvers does not apply to at least one large manufacturer of this type of bus. Their applications have been of nonventilated construction except for a few special applications. Even though inspection covers are not gasketed, experience has demonstrated that dust accumulations are considerably less than in those where intentional ventilation is provided. The result is longer intervals between maintenance periods and less likelihood of interchange of gases between phases.

The authors point out that one of the

weaker points of segregated phase construction is the possibility of phase-to-ground arcs burning through one of the metal barriers. Tests made on similar structures, constructed of 1/8-inch thick steel, nonmagnetic steel and copper alloy at various current ratings, indicate that the hazard from this is relatively slight. On currents as high as 60,000 amperes enduring for 23 cycles, no punctures occurred in separating barriers. Fault tests within the enclosure at full voltage are important to the design of such structures, as the amount of gas created introduces problems which the authors apparently have not considered.

In their discussion on installation, the authors point out the flexibility between various sections of both the bus and the enclosure. The flexible joints in the bus at each section undoubtedly provide this. However, it is not at all apparent how this flexibility exists between the enclosures. They are coupled by a relatively short clamping ring which has to fit tightly to give an air-tight construction. This would mean that accurate alignment of the enclosures was almost a necessity.

Another thought on these bus structures, which merits consideration, is an arrangement by which the housings could be put up and the busses put into them. This would mean that long lengths of bus could be used in many cases and a great number of joints eliminated. A reduction in the number of joints is always desirable and consideration of this problem has indicated that such an installation is possible and at no sacrifice in total installation cost.

R. B. Shores: The discussion presented by Pierson is appreciated as it presents several design features of the new bus which were not accounted for in sufficient detail in the paper.

The quantity of louvers, or deliberate perforations of some type, in existing designs of bus runs can frequently be decreased and even eliminated by increasing the size of the conductor. However, deliberate perforations for ventilation do not represent the total openings in housings constructed of nongasketed metal plates, and the amount of dust deposited on insulators in such housings becomes a matter of degree. The new design is definitely differentiated from a ventilated line of bus enclosures by having all joints in the enclosure made dust-tight by means of gaskets.

We agree with Pierson that fault tests within enclosures are important in the establishment of designs. Field experience, as well as tests made in the high-current laboratory to investigate what happens when an arc-to-ground does occur on a bare bus inside an enclosure, have indicated that an arc travels very quickly along the bus to a barrier, or corner, and does little burning along the length of bus traversed. However, unless interrupted within a matter of cycles, the arc will burn through the housing at a point where it tends to hang. It is true that the amount of gas created in the enclosure is a major problem, and unless the arc is of very low magnitude or unless the arc is interrupted very quickly, no metal enclosure of any shape or practical thickness is going to contain the fault. Since the gas pressure is on all walls of the enclosure, there is as equal a chance on a single line-to-ground fault of the interphase wall giving way as of the outside wall.

In designing the new bus, it was felt that no attempt should be made to hold this pressure to the point of rupture of the housing, which might result in injury to personnel or damage to adjacent equipment by flying metal covers. It has been shown by actual tests that the new design, which uses hinged covers held on by lugs instead of through-bolts, will relieve internal pressures by bowing out of the center of the housing sufficiently to permit the center of the cover to slip from under the lugs and itself bow up; however, the covers in all tests were retained by the hinges and lugs at the cover ends. After each internal fault test, the enclosure sides were straightened in place, and the covers removed, straightened, and replaced. In each internal fault test the arc moving along the bus produced a traveling pressure wave subjecting the entire length of the test run to internal pressure.

The circular flange of each housing end plate has a recessed groove into which a gasket of circular cross section fits. The banding ring and circular cross-section gasket permits axial and angular misalignment of the housing; the length of the banding rings, together with the designed clearance between the circular flanges of the connected end plates and the designed clearance between the banding rings and flat surfaces of the end plates, permits longitudinal variation in spaces between individual housings.

The most important length of conductor within a station is usually the main bus directly connecting together a group of power circuit breakers, yet in a modern installation of factory-assembled metal-enclosed circuit-breaker units this main bus actually consists of short sections, each approximately the width of the circuit-breaker unit, connected together with some type of splice plate. This fact eliminates any hesitancy in the use of short conductor sections. However, there is a very great advantage in the use of short sections of conductor from the standpoint of provision for expansion and ease of alignment, especially in the use of heavy tubular or channel shaped conductors. The alignment of two conductor supporting stations per length of conductor is a relatively simple matter in comparison to the use of three or more supporting stations since supporting stations must be sufficiently aligned to permit slip of the conductor for thermal expansion or contraction and yet not loose to the point of rattle (which would also result in impact stresses on the porcelains on high momentary currents). The alignment problem of more than two stations is also complicated by the fact that copper shapes are not exact in size and true in straightness. In addition to simplifying the alignment problem and of permitting factory-assembled sections to be shipped with the conductor in place, the use of short sections permits ready removal or insertion of any copper conductor after installation of the housings if necessary.

Circuit Breaker Modernization and Faster Fault Clearing on Single-Phase Electrified Railroad

H. F. BROWN
FELLOW AIEE

H. A. TRAVERS
ASSOCIATE AIEE

C. A. WOODS, Jr.
MEMBER AIEE

Synopsis: The circuit breakers designed in 1913 for sectionalizing the 11/22 kv 3-wire trolley and feeder circuits on the New Haven Railroad electrification had become inadequate by 1936 because of limited interrupting capacity to clear line faults fast enough to insure system stability and reliability. Although these were never designed to handle the entire fault interrupting duty, space and weight limitations in the switching stations and supporting structures ruled out their replacement with modern circuit breakers of conventional design adequate for faster fault clearing. The application of modern interrupters to the original circuit breakers, together with the redesign of tanks, operating mechanism and supporting frames (with spring supports), produced a circuit breaker of approximately the same over-all dimensions and weight which by high-power laboratory tests proved to be adequate to interrupt the entire fault current (instantaneous rms symmetrical) as determined by a-c network calculator studies of the system. These studies were also the basis of application of instantaneous overcurrent relays for fault clearing by the line circuit breakers, of which there are over 500, making this system one of the largest installations with this type of relay protection. An analysis of the network calculator studies, the tests of the redesigned circuit breaker, and the operating results with their application are here reported.

The Railroad Sectionalizing and Original Fault Clearing Scheme

THE 11/22 kv 3-wire system of power transmission and distribution for the single-phase a-c electrification of the New York, New Haven and Hartford Railroad between New York and New Haven, Conn., was placed in service in 1914, seven years after the initial a-c electric operation, and has been described in more detail in a recent Institute paper.[1] There are over 200 separate sections of through trolley and feeder circuits protected at each end, together with about 50 radial or stub-end trolley circuits, and 100 bus and tie circuits, all protected by approximately 525 outdoor oil circuit breakers of special design. The 4-track line is sectionalized at interlocking and track switching points where signal towers are located. The circuit breakers were designed to be mounted directly over the tracks and trolley system, on the adjacent catenary anchor bridges, which were specially designed to carry this equipment, see Figure 1.

These bridge-type circuit breakers, so called because of their location, had an interrupting capacity of about 3,000 amperes at 11 kv, and the associated relay system was so designed that no line circuit breaker was opened under fault conditions until the fault current had been reduced to a safe interrupting value (between 2,500 and 3,000 amperes) by the automatic insertion of resistance in series with the line at the power supply points. The insertion of these resistances, which were of values several times the maximum line impedance, had the effect of greatly reducing the fault currents. In all through parallel circuits, except the faulted circuit, the reduced fault currents approached nearly uniform values below the pick-up setting of the type *B*, over-current, line circuit breaker relays. This permitted all the relays which picked up on the initial high values of fault current, except those controlling the one or two circuit breakers directly feeding the fault, to drop back during the interval between the instant of fault and the energization of a common trip-power circuit from the power station to all switching stations. Any relay remaining picked up would then allow its associated circuit breaker to be tripped from this trip-power circuit.

The resistance was inserted in both the trolley and feeder circuits at the power supply points by a main over-current relay of special design, manually adjustable in its setting, which was kept slightly greater than the maximum expected load swing. This relay picked up on faults and caused a by-pass circuit breaker to open, which normally short circuited the resistance, always in series with the line. An auxiliary "*b*" switch on this by-pass circuit breaker energized the closing coil of a circuit breaker which energized the 11-kv automatic control wire. This furnished tripping power through small 11,000/110 volt transformers at all anchor bridge switching stations (see Figure 2). The bridge circuit breakers were thus tripped by 110-volt 25-cycle alternating current. They were reclosed by 110-volt 25-cycle alternating current from another circuit, normally energized. In many cases closing at 440-volt alternating current was used because of the greater distance between the signal station and the anchor bridge on which the circuit breakers were located.

The line circuit breaker relays and associated manual control were located on switchboard panels in the signal towers adjacent to these switching stations, of which there are about 30 in the electric zone, controlling 4 to 38 circuit breakers each. This fault clearing scheme had the advantage, found in pilot-wire relay

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H. F. Brown is with the New York, New Haven and Hartford Railroad, New Haven, Conn., and H. A. Travers and C. A. Woods, Jr. are both with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

Acknowledgment is hereby given to L. L. Fountain, switchgear engineer, Westinghouse Electric Corporation, for his valuable assistance in the preparation and editing of this paper.

Figure 1. Anchor bridge switching station
Left—Before rehabilitation
Right—After rehabilitation

schemes, of causing the two circuit breakers associated with any faulted circuit to trip simultaneously, a very desirable feature in the earlier days of inductive coordination with neighboring communication circuits.

On later extensions of the railroad electrification, a local definite time-delay relay was applied to the live tripping-power source at each switching station. This provided time, before automatic tripping, for the insertion of the power supply resistors, thus eliminating the necessity for the extension of the automatic control wire. With careful timing, practically simultaneous tripping of circuit breakers on through circuits resulted.

The great disadvantage of the resistance-insertion scheme was the relatively long time interval required to clear a fault, usually about 16 to 18 cycles (on a 25 cycle basis) or about 0.7 second for all the associated apparatus to function in sequence.

Need for Faster Fault Clearing

When the power supply, from the railroad-owned and operated station at Cos Cob, Conn., was augmented in 1915 by purchased power from the New York Edison Company at West Farms (East 174th Street, New York), this same resistance insertion scheme was used at that supply point. Later, in 1927, when three additional supply stations using frequency changers were installed, these also had the resistance-insertion scheme incorporated in their circuits.

It was soon found that the resistance insertion under fault conditions greatly impaired the system stability; and frequency changer stations would often drop out of step on faults. On severe faults, the Cos Cob station would sometimes drop out of step with the New York Edison supply, especially after 1936, when this supply was transferred from a segregated 25 cycle machine to the 25 cycle bus at the Hell Gate station of that system. By this time the railroad system loads and swings were increasing in magnitude, and fault conditions were becoming more severe. The protection scheme was becoming inadequate, and faster fault clearing was clearly indicated.

The matter of securing larger capacity single-pole circuit breakers for instantaneous tripping was investigated by the railroad with various manufacturers in 1935. It was found that new circuit breakers of conventional design and of the capacity required could not be located on the anchor bridges as originally designed, and that such circuit breakers could be used only by the complete redesign of switching stations, involving a very considerable expense. A great advance in the art of circuit breaker design had been made since the date of design (1913) of the bridge-type circuit breaker, and the matter of applying some of these improvements in design to the existing circuit breakers to improve their performance had already been discussed with the manufacturer of these circuit breakers.

Original Circuit Breaker Design

The bridge type circuit breaker was quite rugged in construction when viewed in the light of circuit breaker design at the time of its manufacture, and is shown in Figure 3. It consisted of a cast iron main base carrying the two terminal bushings and current transformers, a nonmechanically and nonelectrically trip-free a-c solenoid mechanism and operating lever system, and a flat bottom, sheet steel tank, elliptical in shape. A removable hood and swinging door of cast iron supported by the main base provided a cover for the solenoid mechanism and operating lever system. The tank was fastened to a malleable iron ring carrying four studs for bolting to the main base, and required 25 gallons of oil for normal oil level. The main base was mounted on two cast iron uprights with diagonal cross ties located at the rear of the circuit breaker. The double-break butt-type contacts consisted of a heavy malleable iron cross-bar carrying both main and arcing plunger-type contacts backed by compression springs. These butted against the single stationary contacts screwed on the terminal bushings. A maple wood lift rod was attached to the operating lever system by means of a short link. It had $5^1/_2$ inches of travel with $5^1/_8$ inches break distance. Many of the circuit breaker terminals were originally porcelain bushings, although some of the last circuit breakers furnished had condenser-type bushings, and later, all were changed to this type. The top cap of these bushings carried the hinge jaws of hook-stick operated disconnecting switches. The contact jaws of these switches were carried on post-type insulators supported by brackets attached to the circuit breaker main base.

Electrical creepage and striking distances both outside and inside were made quite liberal because the ground (and all grounded apparatus) was one side of the trolley-rail circuit. Also, the circuit breakers being mounted directly over the tracks, were subjected to the soot and steam from the occasional operation of steam locomotives; and the salt atmosphere and highly repetitive duty imposed on the circuit breakers all demanded these liberal allowances.

The normal manual switching operations and frequent automatic fault clearing service performed by these circuit breakers was unusually severe, compared to normal power line service, occasionally

amounting to several operations per day on some circuit breakers. Nevertheless, they gave satisfactory performance for more than 25 years.

First Application of Modern Interruptors

Those familiar with the history of circuit breaker development know the great strides made in controlled arc interruption during the period 1928 to 1933. The deion grid[2,3] interruptors were developed during this period, and successfully applied to a large variety of oil circuit breaker designs. These new interruptors could be applied to circuit breakers in service almost as readily as to new designs, with very great improvement in performance, and reduction in contact maintenance and oil deterioration.

In 1933, the New Haven Railroad indicated interest in the possibilities of applying these modern interruptors to some of their bridge-type circuit breakers, at points where the duty seemed most severe. Investigation showed that it would be possible to adapt a light duty deion grid interruptor with finger contacts to this design of oil circuit breaker, and two of these circuit breakers were so equipped and placed in test service at Stamford, Conn., on a radial circuit feeding the New Canaan branch.

A minimum number of new parts consisting of stationary and moving contacts complete with Micarta wood lift rod, lift-rod guide, and an accelerating spring were supplied. After two years of service, with improved performance and reduced maintenance, the railroad felt justified in equipping about 80 circuit breakers, all located at the power supply points where the interrupting duty was the most severe, with these new contacts. This program was completed in 1938.

Inadequacy of A-C Closing Solenoid

In December, 1938, one of the bridge-type circuit breakers then equipped with deion grid contacts and located at the Cos Cob power supply point was subjected to two successive operations in clearing a near-by fault. On the second operation, which was a routine, manual, "CO" (close-open) operation, the operator observed considerable demonstration on the part of the circuit breaker. The fault was cleared, but with expulsion of considerable amount of oil and gas. Inspection showed contact burning in excess of that expected from interruption of two 3,000 ampere faults. Further investigation indicated that this was due to closing-in against the high fault current rather than the interruption of the lower value with resistance inserted. This closing-in difficulty was attributed to the inherent characteristics of the a-c closing solenoid which lacked sufficient power to close and latch the contacts against the magnetic blowout forces of heavy fault currents.

In order to verify the foregoing analysis and to determine if d-c operation of the closing solenoid would adequately rectify the closing-in difficulty, a deion grid circuit breaker was returned to the manufacturer for laboratory tests. Arrangements were made to obtain closing-in tests with either 440-volt 25-cycle control voltage, or direct current supplied from a Rectox rectifier impressed on the closing solenoid. The circuit breaker was closed-in against low-voltage short-circuit currents over a range of 4,100 to 33,100 equivalent rms amperes. A total of 12 operations with the a-c closing power indicated that the circuit breaker could close successfully against 5,000 amperes, but would fail to close against 10,000 amperes. A total of 20 operations with the d-c closing power were all successful up to the maximum value of 46,200 amperes, crest value of the first current peak. Figure 4*A* is an oscillogram of a typical failure to close with a-c control, and Figure 4*B* is one for a successful closing with d-c control. In both cases the equivalent rms value of the fault current was approximately 20,000 amperes.

Need for Additional System Data

Although the circuit breakers, newly equipped with deion grids, were giving the expected improved performance with reduced arcing time, the over-all fault clearing time of approximately 3/4 second was not materially reduced. The major portion of this time was consumed by the resistance-insertion scheme already described. The only way of reducing this time would be to make the line circuit breakers trip instantaneously; thus re-

Figure 2. Diagram of 3-wire system showing sectionalizing arrangement and the former resistance. Insertion scheme for fault clearing (now abandoned)

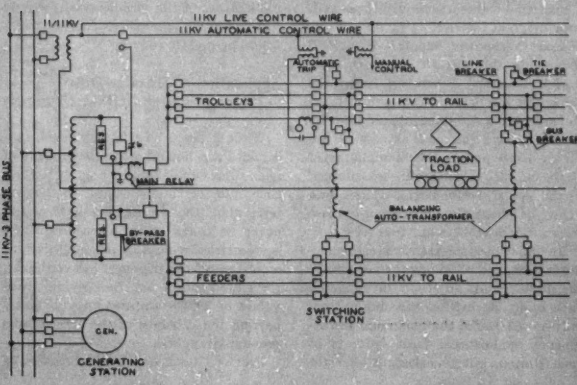
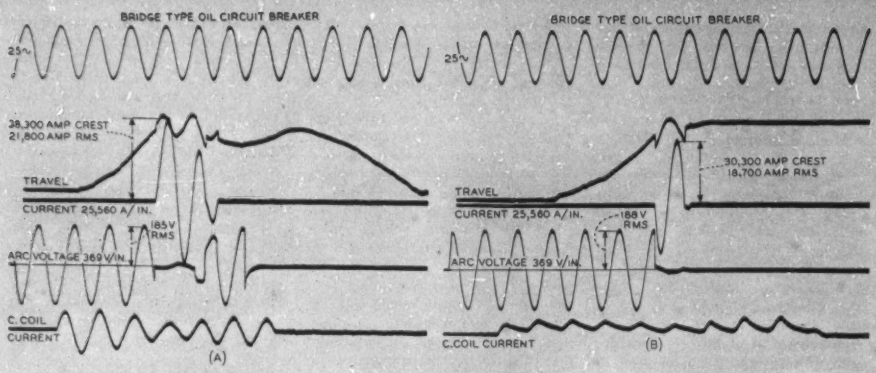


Figure 3. Bridge-type circuit breaker as originally manufactured



A—Showing inability to close with alternating current on solenoid mechanism

Figure 4. Typical oscillograms of low-voltage closing-in tests

B—Showing ability to close with direct current on solenoid mechanism

quiring them to interrupt the total fault current. System stability and increasing loads were making this change more and more imperative. This raised the question of circuit breaker interrupting requirements under system fault conditions. Accurate data on system fault currents were not available, but rough calculations indicated that at the major sources of power the fault currents might approach 25,000 amperes.

The deion grid contacts already installed on some of these circuit breakers were more than adequate for interrupting the 3,000 amperes required under the existing fault clearing arrangement, but they had not been applied with the view of any major change in interrupting requirements. Laboratory tests and field experience with this interruptor in modern circuit breakers had proved its adequacy for the intended service, but the interrupting limit when installed in a single-pole slow-speed 24-year-old circuit breaker for 11-kv 25-cycle service, had never been demonstrated. It was obvious that it would not even approach the possible 25,000 ampere requirement, equivalent to a 700-mva 3-phase interrupting rating. New circuit breakers of modern design of this rating were of dimensions and weight that precluded any possibility of their use in the existing switching stations. The only alternative left the railroad was to further explore the possibilities of making these old circuit breakers as adequate as possible. It was then obvious that accurate data on fault currents, system stability, and general system characteristics were required before any satisfactory and economic solution of the problem could be obtained.

A-C Network Calculator Studies

In June, 1938, it was decided to study system fault conditions on the a-c network calculator. Some previous calculations had been made in 1913, in connection with inductive co-ordination studies, of fault conditions existing at that time. From these earlier studies, augmented by data and methods outlined by A. W. Copley[4] in 1920, fairly accurate data were obtained on trolley-rail, feeder-rail, trolley-feeder, and their mutual impedances. From the various manufacturers the necessary data were secured on line auto-transformers, station step-up transformers, and generator impedances. A complete network impedance diagram of the system was laid out, and set up on the a-c network calculator at East Pittsburgh for existing and possible future conditions of maximum generation.

Figure 5*A* shows a section of the impedance diagram for circuits in the vicinity of Cos Cob power supply. Figure 5*B* shows how this was set up on the network calculator. Figure 6*A* shows the value of a bus fault in rms, symmetrical amperes, and its components obtained by reading around the mesh circuits shown. Figure 6*B* shows how the values obtained on the network calculator are transformed to the actual 3-wire circuit values, and Figure 6*C* shows the application of these values to four trolleys and four feeders.

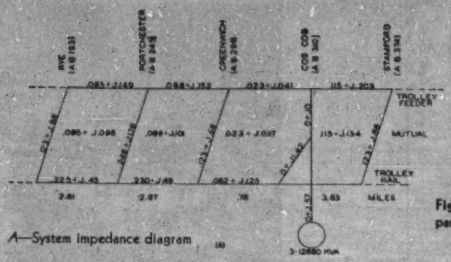
By taking bus faults at each sectionalizing station, and obtaining the currents in the mesh circuits adjacent thereto, sufficient basic information was obtained not only to determine the maximum instantaneous symmetrical fault currents at each station, but to calculate later the proper relay setting for clearing faults on all circuits.

Figure 7 sets forth in diagrammatic form the value of the fault current at various stations under maximum generating conditions. It will be seen from this diagram that the maximum interrupting duty on any line circuit breaker is at Cos Cob, where the value of the instantaneous symmetrical fault current is about 16,000 amperes for a trolley-rail fault, and about 14,000 amperes for a trolley-feeder fault. Therefore, if the bridge-type circuit breakers could be redesigned to interrupt these values, they would fulfill all requirements for instantaneous operation, and the resistance-insertion scheme could be abandoned. The a-c network calculator studies showed further that if faults could be cleared within the first quarter of a second (6 cycles) the stability of the system would be materially improved, and the chances of the various power supply stations falling out of step during faults would be greatly reduced.

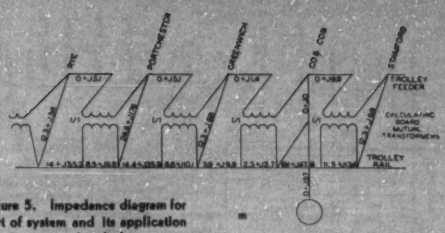
Application of Heavier Duty Interruptors, and Other Changes

While it could be safely assumed, taking into account all associated factors of relay time, circuit breaker contact parting time, and the time constants of the system, that the circuit breakers would never be called upon to interrupt the asymmetrical values of the fault current on instantaneous tripping; nevertheless, the interruption of rms symmetrical values of 16,000 amperes was obviously beyond the capacity of the interruptors previously applied.

Previous tests on circuit breakers of



A—System impedance diagram



B—A-c network calculator impedance diagram for (A)

Figure 5. Impedance diagram for part of system and its application to network calculator

modern design had indicated that heavier duty interruptors and greater contact travel at higher speeds than used for the original modification of these circuit breakers would be required for such service. Even with these features, there was considerable doubt as to whether the cast-iron main base, and other cast-iron components would withstand the increased mechanical forces to be encountered in such service. Nevertheless, it was considered worthwhile to modernize, and to make laboratory tests on a sample circuit breaker to determine the necessary changes.

The following minimum quantity of new parts was deemed necessary to give an adequate circuit breaker.

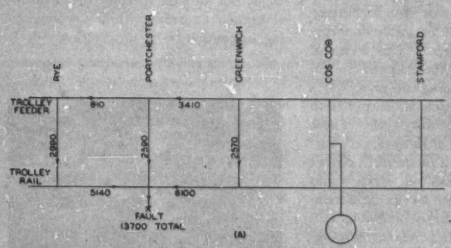
1. New type *F*, 23-kv deion grid contacts.
2. New levers, lever supporting frame, and mechanism hood to increase travel to approximately eight inches.
3. New stronger and deeper tanks requiring approximately 30 gallons of oil.
4. New accelerating device and oil dashpot to obtain increased speed and prevent excessive rebound on opening.
5. New six-pole auxiliary switch, operating levers, and housing to isolate switch from the solenoid mechanism and lever compartment.
6. New electrically trip-free control relay and housing.

Laboratory Tests on Revised Bridge Type Circuit Breaker

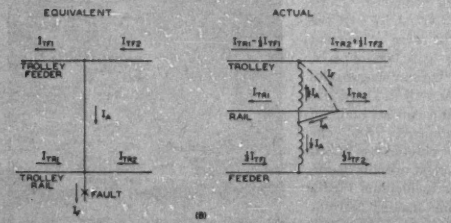
In view of the operating duty required on the circuit breakers and the fact that the revamped circuit breakers would be of non-oil-tight construction, the test schedule was set-up on the basis of an 0-2 minute "CO" (close-open) circuit breaker duty cycle. These laboratory tests were to be made at 11-kv 25-cycle single phase, over a current range up to 16,000 amperes, rms, interrupting; and 40,000 amperes, crest, first peak, closing. Several series of closing and interrupting tests were made to determine the necessary modifications required to obtain a modernized circuit breaker which would give satisfactory performance in the proposed service.

The first series of tests were made on a modernized circuit breaker utilizing the new parts previously listed. Closing was from a 100 volt d-c source, and tripping was from a 114-volt 25-cycle source; both operations being controlled by the laboratory motor operated cam switches. On the seventeenth operation, a "CO" shot, the circuit breaker contacts froze when closing-in against a first 1/2-cycle peak of 38,400 amperes, crest. The sticking of the contacts was due to rough contacts resulting from the 16 previous operations without contact inspection or maintenance.

The next series of tests was similar to the first series, except closing power was obtained from a 440-volt 25-cycle Rectox

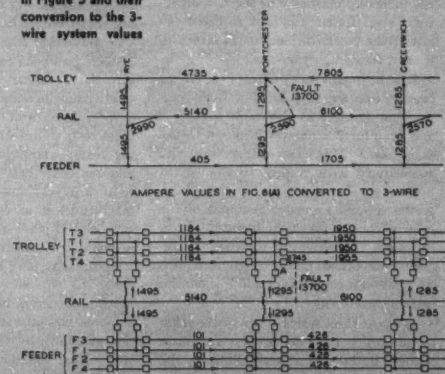


A—Fault current values from network calculator



B—Method used to convert values in A to 3-wire system values

Figure 6. Values found on network calculator for a mesh in Figure 5 and their conversion to the 3-wire system values



C—Actual fault current distribution

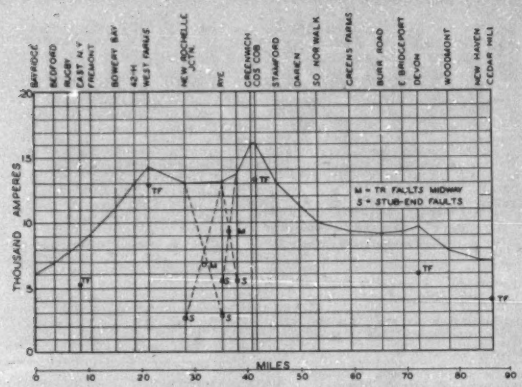


Figure 7. Curve showing values of trolley-rail and trolley-feeder fault currents at various parts of system

rectifier adjusted to give 110 volts direct current on the closing coil. The control of the circuit breaker was modified to utilize the new a-c electrically trip-free control relay and auxiliary switch which had been added. This second series of tests ended when both cast-iron supporting frames and a corner on the main base casting were broken. On this test the circuit breaker closed-in against a peak current of 51,000 amperes, crest, and interrupted 14,300 amperes, rms.

The broken main base, repaired by welding, was mounted on a new structural steel supporting frame for the next series of tests. This casting again cracked on a high current shot, making it evident that the increased elasticity of the new structural steel frame was not sufficient to absorb the shocks incident to the interruption of these high currents.

For the fourth series of tests the circuit breaker unit was suspended between compression springs, as shown in Figures 8 and 9, to further relieve the stress on the main base. This series of tests was quite satisfactory with respect to the circuit breaker structural details, but the oscillograms showed excessive rebound of the moving contact on the high current interruptions. Investigation revealed this rebound was caused by the differential pressure between the contact and mechanism ends of the lift rod.

The next three series of tests were made to determine the effectiveness of minor changes found desirable to eliminate the excessive rebound, reduce oil loss, and to prevent drainage of oil onto the bridges on which these circuit breakers were mounted. These changes included replacing the all wood Micarta lift rod with a combination steel and wood Micarta lift rod, reducing the baffling around the lift rod and lift-rod guide, replacing the circuit breaker compartment open vent with a baffled vent, and adding an oil conservator to trap the oil draining to the bottom of the mechanism compartment.

The eighth and final series of tests was made on the circuit breaker modernized with parts made from the drawings as changed for manufacture in quantity and as shown by Figures 8 and 9. During this time, field studies indicated that d-c control from a 115-volt storage battery would give the most desirable and economical control source. This series of tests was made with the changed control and the operations were made on a "CO"-2 minute —"CO" duty basis instead of an "O"-2 minute —"CO" duty cycle. These last series, consisting of 19 tests, gave further indication that a modernized circuit breaker had been produced, which would give very satisfactory service at a relatively small increase in size and weight.

A brief review of the foregoing development tests shows a total of 109 interrupting tests; about half being "O" shots, and the other half "CO" shots. Of the total interruptions 62 were at current magnitudes in excess of 10,000 amperes, rms, and 47 were at current magnitudes below 10,000 amperes. Of the latter, 19 interruptions were in a current range of 5,000 to 10,000 amperes, rms. The maximum current interrupted was 22,800 amperes, rms on a straight opening operation as shown by the oscillogram Figure 10*A*. The maximum making current was 54,400 amperes, crest, on a "CO" operation in which the current interrupted was 15,200 amperes, rms. The oscillogram shown in Figure 10*B* shows a typical "CO" operation in which the circuit breaker closed against 52,800 amperes, crest, and interrupted 16,400 amperes, rms. A careful analysis of the oscillograms on the higher current interruptions showed average impulse pressures of approximately 60 pounds per square inch, and only six shots with pressures in excess of 150 pounds per square inch. These higher pressures were not confined to the highest current interruptions, but all were incident to a high instantaneous current with an accompanying discontinuity of arc voltage. It is

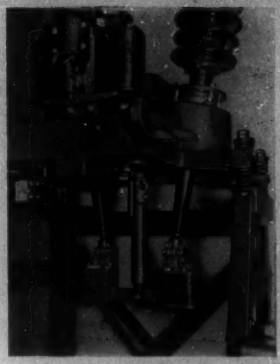


Figure 8. Contacts and details of modernized test circuit breaker

Figure 9. Completely assembled modernized bridge-type circuit breaker

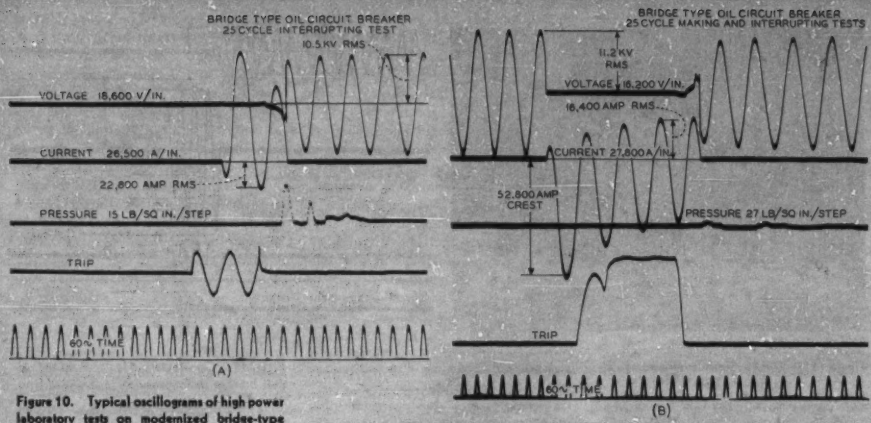


Figure 10. Typical oscillograms of high power laboratory tests on modernized bridge-type oil circuit breaker

A—Interrupting 22,800 amperes, rms

B—Closing against 52,800 amperes, crest and interrupting 16,400 amperes, rms

interesting to note that the operation having the maximum impulse pressure was perfectly satisfactory, both mechanically and electrically. Of the total of 109 operations, there were no electrical failures on interruption, and only one electrical failure on closing-in due to rough contacts, as previously noted in the first series of tests.

Rebuilding the Circuit Breakers by the Railroad

Having demonstrated by the foregoing comprehensive series of tests the requirements for the adequate modernization of the bridge-type circuit breakers, the manufacturer assembled a sample circuit breaker, and furnished, on the railroads' subsequent orders during the next six years, the required parts for the rebuilding of approximately 525 circuit breakers by the railroads' maintenance forces. This presented an extensive undertaking not only in removing the old circuit breakers from service, dismantling, reconditioning usable parts, which included machining the main-base castings, but in reassembling with new parts, testing, and replacing on the anchor bridges and reconnecting for service. All of this was accomplished without any interruption to railroad service.

This required very careful planning. At the Cos Cob power plant, part of an old boiler house was vacant and used only for storage. This was cleared and equipped with work benches for the assembly work, and arrangements were made whereby any machining of bases, and so forth, could be done in the power plant machine shop. Both alternating and direct current were available for testing, and adequate space was made available for parts storage, small stores and tools.

Before any switching station was taken out of service, conditions were reviewed with the local communication companies as to inductive co-ordination with the balancing transformer out of service at that station for an extended period of time. In all cases it was found possible to allow this which greatly simplified the field work, since all through trolley and feeder circuits could then be tied through with disconnecting switches, and the high tension connections could be removed one by one from each circuit breaker until the entire station was disconnected. The circuit breakers were then dismantled on the bridge, the parts to be reutilized carefully taken down and removed to Cos Cob, and all the old unused parts scrapped. In the cases where the anchor bridge switching stations were at the power supply points, only half the circuit breakers could be removed from service at a time, in order not to interrupt the power supply. The dismantling usually required about one week. This was followed by such maintenance work and rearrangement of supporting structures required after 25 years of service and to get improved clearances, and so forth, which required between two and three weeks more. In cases involving stub-end feeds, some temporary connections were required.

When this field work was completed, the gang was moved to Cos Cob for circuit breaker assembly. It took about six weeks to assemble 20 circuit breakers, the average number at a switching station. Figure 11 shows the assembly work.

The reassembly accomplished, the circuit breakers were tested, mounted on skids, with terminals crated, but without disconnecting switches or bracket insulators, loaded on flat cars and returned all together to their switching station. There they were lifted onto the bridge deck, skidded into place, further assembled with disconnecting switches, and reconnected to their high-tension and control circuits, all of which took another month or longer, depending on weather conditions. Before restoring the station to service, the circuit breakers were given a high potential test, the bushing current transformers were tested for correct connections and polarity, the bus differential protection (not described in this paper for lack of space) was checked, and each circuit breaker was timed with cycle-counter for tripping and closing time.

The first station was taken out of service in July 1940, and restored to service with new circuit breakers in the following October. The first five stations were revamped as to circuit breakers without undertaking any change in the control or relay scheme other than to substitute a 55-cell 80-ampere-hour storage battery for the former 110-volt a-c tripping and closing power, and to place any stub-end feed circuits on instantaneous trip. The resistance-insertion scheme was still used on all through circuits pending the completion of a new relay scheme then in the process of being developed.

Figure 11. Circuit breaker rebuilding assembly line

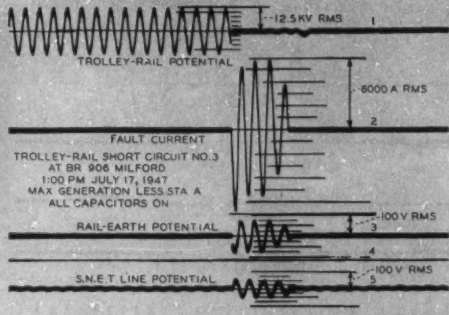


Figure 12. Oscillogram of fault current and circuit breaker performance on prearranged fault

Redesigning the Fault Clearing Relay Scheme

The network calculator studies had indicated the magnitude of the component currents feeding a faulted bus from circuits in either direction, and from the local transformer.

Further calculations were made of the single circuit impedances between switching stations. These were added to the values for system impedance to the adjacent switching stations, found from the network calculator values of total fault current at that station. This gave the maximum impedance for such stub-end feed faults that might occur due to the possible sequential operation of circuit breakers on a through circuit. Similar approximate values of impedance were calculated for mid-way faults in the electrical center of through circuits, for insuring simultaneous tripping of both circuit breakers feeding such faults. Typical values of such faults are indicated in the diagram shown in Figure 7.

A careful survey of these calculations indicated that there were enough parallel circuits and the sections were sufficiently short so that with fairly accurate impedance data, instantaneous over-current relays could be used, and set high enough to operate only for faults in the section of trolley or feeder controlled by its associated circuit breakers. On some of the longer sections (of approximately seven miles) the relay settings are not very much above the maximum train load requirements, but the operation on instantaneous trip has shown an exceptionally high degree of selectivity. There have been very few operations of circuit breakers not directly in faulted circuits, unless there were unusual conditions of more than one paralleling circuit out of service.

Discussion with the communication interests during the early part of the development of the instantaneous overcurrent relay trip scheme revealed that sequential tripping of through circuits with this scheme, because of faster clearing, was less likely to cause inductive disturbances than the slower resistance-insertion scheme.

Beginning with the fifth station revamped (in 1942) each station was equipped with two relay trip busses, to which any or all relays could be connected. One bus was energized by a local definite time-delay relay for use with the resistance-insertion scheme, and the other was normally energized, for instantaneous trip.

The old type *B* relays were replaced by new type *SC* over-current relays. The settings were left the same as the old type *B*, and all were connected to the delayed tripping bus. By June, 1944, all the stations between Cos Cob and West Farms had been changed to new circuit breakers with new relays, and then, in rapid succession, the relays on adjacent bridges were reset to calculated settings and connected to the instantaneous trip bus until all were changed. Thereupon, the resistance-insertion scheme was cut out of service between Cos Cob and West Farms. In due time all resistance-insertion in circuits west of West Farms and east of Cos Cob was entirely abandoned.

The war had a serious retarding effect on the revamping program, necessitating but a skeleton maintenance force to do all the work, although the material was allowed to come through, as essential, on the required priorities. There was not a single serious accident chargeable to this work, even though much of the field work was performed over or near energized 11-kv trolley and feeder circuits.

Operating Results With Rebuilt Circuit Breakers

The first sample circuit breaker went into service in the latter part of 1940, in the yard at Port Chester, a zone where the symmetrical fault currents were on the order of 13,000 amperes, rms. In the following three years this circuit breaker interrupted 58 short-circuits and had approximately 1,200 mechanical operations, most of the latter being for test and isolating purposes. In this period, the arcing horns were renewed five times, and the stationary and moving contacts were renewed once. Other maintenance included dressing of contacts, checking adjustments, and renewing oil. On two occasions the deion grid stack lower plates on the line side were found cracked, and were replaced. Of two other circuit breakers in similar service at different locations, one circuit breaker had one stack with the lower plates cracked, while the other circuit breaker did not. The cracked plates were due to the excessive pressures which occasionally occur as pointed out under the comments on the laboratory tests. While the occasional cracked plates did not result in any failure to interrupt, a re-enforcing plate was inserted in the bottom of the deion grid stacks furnished during the latter part of the program. These re-enforced stacks were immediately placed on the two yard circuit breakers and they have given no further trouble. However, due to infrequent trouble and satisfactory operation in the last four years, it has been unnecessary to add the re-enforcing plate to the deion grid stacks already installed. In order to increase the life of the parts subject to arcs and arc gases, the area of the

contacts protected by arc-resistant alloys was increased, and renewable inserts were added to the tank liners.

Two circuit breaker failures have occurred since the start of the program. One failure was experienced during the hurricane in September, 1944, due to excessive moisture getting into the circuit breaker after three successful interruptions within a couple of hours. A second circuit breaker failure occurred in 1947, when on an automatic trip-out, the circuit breaker hung-up in a partially open position and thus failed to clear the circuit; requiring clearing from the back-up protection.

The entire system with over 525 circuit breakers was rehabilitated by March, 1947, and operating on the new instantaneous relay scheme, with excellent performance. Figure 12 shows an oscillogram of fault current and circuit breaker performance for a prearranged fault midway between two adjacent switching stations at Devon and Woodmont, Conn. Some sequential operation of circuit breakers is noted, but the entire fault clearing time is but four cycles. Five separate faults were made on this occasion, all of which showed practically identical circuit breaker performance.

The outstanding features of this modernization program is that by a series of laboratory and field tests, a 25 year old circuit breaker and protection system was completely modernized to satisfactorily handle six times the interrupting duty and reduce the fault clearing time from 0.7 to 0.2 second. This improved performance has been accomplished with very little increase in weight and space. Due to the mechanical structure of the old circuit breakers, preliminary studies did not show much probability of obtaining the desired results. The present satisfactory system performance appears to have justified the co-operative and persistent efforts required to complete this project.

The cost of rebuilding these circuit breakers was about half the cost of new single pole circuit breakers of modern conventional design adequate for the service; and the switching station changes were but incidental as compared with what would have been required with new circuit breakers. The new relay scheme added approximately 10 per cent to the total cost.

As the result of this rebuilding program, the railroad now has line circuit breakers with six times their former interrupting capacity, with faster fault clearing, reduced maintenance cost, and improved system stability and reliability.

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Discussion

W. M. Leeds (Westinghouse Electric Corporation, East Pittsburgh, Pa.): No mention has been made of the effect of system frequency on the problem of raising the interrupting capacity of these bridge-type circuit breakers. It has been observed by those familiar with circuit breaker testing that modern oil circuit breakers, capable of interrupting fault currents with relatively short contact separation, show increased variability in performance as the circuit frequency is lowered. The reason is tied up with the fact that a-c oil circuit breakers interrupt at a normal current zero, and the maximum "waiting time" for a current zero after reaching the minimum contact gap for interruption is proportional to the period of the current wave. Let us assume that the minimum arcing time for a given circuit breaker is 0.03 second, corresponding to a contact gap of two inches. The most unfavorable condition is represented by an arc reignition just ahead of this point in the stroke, extending the arcing time by one-half cycle to the next current zero. At 60-cycle frequency this would add 0.0083 second or 28 per cent to the minimum time, whereas at 25-cycle frequency the increment would be 0.02 second or 67 per cent more arcing. On the basis that the arc energy varies as the square of the arcing time (which assumes a linear increase of arc voltage with time) a little calculation shows that the maximum arc energy at 60-cycle frequency would be 63 per cent greater than the minimum, but at 25-cycle frequency 178 per cent greater. The ratio of the maximum energies is 1.7 to 1, suggesting the need of a circuit breaker having 70 per cent greater rupturing capacity when used at 25-cycle frequency rather than at 60 cycles. While these figures are only approximate, they show why a duty of 16,000 amperes at 11 kv single phase (equivalent to 460,000 kva* at 19 kv three phase) required reinforced construction in standard 23 kv 500,000 kva interrupters so that satisfactory performance at 25-cycle frequency might be assured under the most adverse conditions. The successful outcome of this revamping program was due to the persevering efforts of many individuals and demonstrated once more the indispensability of a high power testing laboratory.

H. D. Braley (Consolidated Edison Company of New York, Inc., New York, N. Y.): The Consolidated Edison Company of New York was directly connected with the improvement program, initiated by the New Haven Railroad, and gave its full co-operation in such work as was required to put into effect successive steps of the improvements.

In the past, insertion of grid resistors in the railroad feeders at West Farms substation was frequently causing unavoidable "pumping" (successive opening and closing) of oil circuit breakers, which normally short-circuited the resistors.

Repeated cycles of pumping on fault conditions were causing frequent operation of resistance circuit breakers, which resulted in fast deterioration of circuit breaker oil and burning of circuit breaker contacts, made of the best grade of material available. This required a rigid schedule of circuit breaker maintenance, covering their inspection and overhaul every six months.

Due to the elaborate scheme of insertion of resistors, and of energizing the a-c tripping source to the railroad bridges, the fault clearing time was relatively high. This, combined with pumping of resistance circuit breakers, was affecting the stability of the interconnection and resulted in frequent separations between the New Haven and the Edison systems.

When the old method was abandoned, maintenance at the West Farms substation was considerably reduced. Grid resistors, resistance circuit breakers and associated equipment were finally retired. The number of separations between the two systems appreciably decreased.

Faster clearing time at the railroad bridges permitted a 40 per cent decrease in the operating time of back up relays at West Farms and reduced the duration of voltage disturbances on the Edison system.

In conclusion, the modernization program of the New Haven Railroad resulted in an excellent solution of a difficult relaying problem. Recognition is made of the fine co-operation which we had from the New Haven Railroad in solving our problems connected with the modernization.

H. F. Brown, H. A. Travers, and **C. A. Woods, Jr.**: Braley's comments indicate that the modernization program described was far reaching; not only improving operation on the railroad system, but also the interconnected systems supplying power to the railroad.

Leeds' discussion emphasizes the possibility of increased severity in interrupting duty imposed on the circuit breakers due to the lower system frequency. While this was not brought out in the paper, there was evidence of this possible increase in energy, both in the laboratory tests and in field service. The 25-cycle system frequency, undoubtedly, made the task of satisfactory circuit breaker modernization more difficult.

* By NEMA Rule SG6-205 B(2).

An Instrument for Recording Continuously the Salinity, Temperature, and Depth of Sea Water

A. W. JACOBSON
MEMBER AIEE

MANY physical phenomena in oceanography can be determined from knowledge of the horizontal and vertical distributions of the salinity and temperature of sea water. Current practice in obtaining salinity measurements makes use of sampling devices and chemical analyses.[1,2] Surface temperatures are measured with conventional-type thermometers and thermographs. Measurements at subsurface levels are made with reversing thermometers[1] and with mechanical "bathythermographs,"[3] which provide a record of temperature as a function of depth. Although these methods provide highly accurate information, the lack of continuity of salinity data may at times limit their usefulness. Also, considerable time is consumed in the procedures of sampling, analysis, and plotting of data, and it is often difficult to retain navigation.

The instrument described measures continuously the temperature and specific electrical conductance of sea water, and computes from these its salinity. The measured temperature, the computed salinity, and the depth at which the measurements are being made are recorded simultaneously on a strip chart.

The instrument operates from a 115-volt 60-cycle supply and is unaffected by normal voltage and frequency variations or by the vibration and motion of the vessel. It measures the temperature of the water over a range from 28 to 90 degrees Fahrenheit with an error not greater than 0.2 degree Fahrenheit. Salinity is determined over a range from 20 to 40 parts per thousand (‰) with an error not greater than 0.3‰. The maximum error in depth over the range from 0 to 1,200 feet is 12 feet. The time lag of the instrument is governed primarily by the thermal speed of response of the resistance thermometer bulb, which has a time constant of 0.45 second.

Although the precision is less than that obtainable by standard reversing thermometers and titrations for chlorinity, this disadvantage is offset for many purposes by the convenience of continuous measurement, and the prompt availability of the temperature and salinity data.

Several earlier instruments for determining the salinity of sea water from conductivity measurements are described in the literature.[4-8] These instruments employed Wheatstone bridge circuits by which the conductivity of a batch or flowing sample of sea water was compared with that of a standard of known salinity contained in a sealed cell while both were maintained at the same temperature. While this method provided a high degree of accuracy, the use of a sealed cell for temperature compensation introduced a serious time lag, and limited the practical application of these equipments.

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A. W. JACOBSON is with The Bristol Company, Waterbury, Conn.

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The author wishes to acknowledge the contributions of Dr. Alfred C. Redfield, Allyn C. Vine, and Dr. William L. Ford and John F. Holmes, all of the Woods Hole Oceanographic Institution, who, respectively, directed the project, initiated the development, and conducted the field tests; and John L. Russell of The Bristol Company, who was responsible for the mechanical design.

General Description

The instrument consists of three major units; the measuring elements assembly, the computing and recording mechanisms, and the amplifier unit. The electrical connections between the units are made with multiple-conductor cables and plug-type connectors, so that no tools are necessary in hooking up the equipments. Figure 1 shows the instrument installed in the Woods Hole Oceanographic Institution research vessel Balanus.

The measuring elements assembly, shown partly in section in Figure 2, includes a resistance thermometer bulb, a conductivity cell, and a Bourdon pressure element. The resistance bulb has a thermal time constant of 0.45 second and is capable of withstanding external pressures in excess of 2,000 pounds per square inch. It is constructed by drawing a number of strands of enamelled nickel wire into a long, small-diameter, thin-walled copper capillary tube, sealing the ends, and winding the tube on an open frame to form a helix. The connections are brought out through one end with kovar-glass sealed terminals. The conductivity cell is made up of a plastic tube sheathed in heavy bronze tubing and includes three coaxial platinum-lined electrodes, one in the center and one at each end. The Bourdon depth element is made of a beryllium copper alloy. Its free end carries a contact which moves across a slide-wire, converting a position measurement to an electrical value which can be easily transmitted. A single 9-conductor waterproof cable connects the measuring elements assembly to the recorder. The several sections comprising the housings for the measuring elements are bolted together, using confined rubber gaskets to make the complete unit pressure tight.

The computing and recording mechanisms are mounted on a steel frame enclosed in a wood case having removable sides and top cover. The top view of this

unit is shown in Figure 3. The measuring and computing mechanisms are electromechanical servo systems. The chart drive mechanism is provided with a speed change lever which can be used to change the chart feed rate from a given number of inches per hour to the same number of inches per minute. In addition, several sets of change gears are provided to permit many different chart speeds to be obtained. The maximum speed is 12 inches per minute. The three recording pens deflect about a common axis, and each uses a characteristic color of ink for making its record. A fourth pen, fixed in position, indicates any lateral departure of the chart, making it possible to correct the readings should a departure occur. The chart has 62 uniform divisions and has printed on it three sets of scales, one for each of the required measurements. One division on the chart corresponds to the following increments; 1 degree Fahrenheit in temperature, 0.2‰ in salinity, and 20, or 10, feet in depth, depending upon the calibration of the depth measuring element. Two salinity ranges are provided; 20 to 32‰ and 28 to 40‰, a switch being used for setting the desired range. The specific conductance of the sea water is indicated by a dial visible through a window in the front of the case.

Four identical amplifiers, housed in a single open-type case, are used to increase the power available from the measuring and computing circuits to a level sufficient for operating the various balancing motors. All of the electrical connections to each amplifier are made with a single plug-type multiple connector.

The mechanical construction of the instrument is sturdy, and it will not be damaged by reasonably rough handling. Where fixed mechanical relationships must be maintained for accurate performance, the units involved are pinned together or to supporting structural members with close-fitting dowels. All metal parts are protected from corrosion by Navy-approved methods for shipboard equipments.

In order to prevent ambient temperature errors all fixed resistors and slidewires in the measuring and computing circuits are wound with resistance wire having a negligible temperature coefficient. To insure long satisfactory performance, all capacitors and transformers used in the instrument are sealed hermetically.

Discussion of Design

Temperature System

The selection of a resistance thermometer for the temperature measuring element was made after considerations were given to various types of systems. These included the resistance thermometer, liquid-filled and vapor-tension type bulbs, and thermocouples. The systems were compared for accuracy, reproducibility, speed of response, scale distribution, and measuring and servo system requirements. The resistance thermometer arrangement was selected because it appeared to offer the best compromise among these factors.

Resistance thermometer bulbs can be reproduced with an accuracy of 0.1 degree Fahrenheit or better, and by suitable design the time constant can be made small, in the order of one-half second. The scale distribution, which depends upon the type of temperature-sensitive wire and the circuit used, can be made nearly linear. A simple Wheatstone bridge circuit energized from an a-c source can be used to measure the resistance of the thermometer bulb.

Figure 1. Shipboard installation of salinity-temperature-depth recorder

Variations in the supply voltage affect only the sensitivity of measurement. A simple a-c electronic amplifier can be employed to provide the necessary output power, and the performance of the system can be made so that large variations in the contact resistance are negligible.

Conductivity System

Laboratory measurements of the electrical conductivity of a solution are usually made with a 2-electrode cell and a Wheatstone bridge circuit. When this type of cell is immersed in a solution the measured conductance includes the conductances of the paths within and external to the cell. Since the cross-sectional area of the external shunt path may be very large for a cell submerged in the sea, its conductance will be large compared to the conductance within the cell. A cell with an electrode at each end may be effectively short-circuited when used in this way.

To avoid this difficulty a cylindrical cell with three coaxial electrodes is used. One electrode is in the center of the cell and one at each end. The two outer electrodes are connected together electrically, and the potential is applied between them and the center electrode. The lead to the center electrode is insulated, and approximately half of the current in this lead flows through the sea water sample to each end electrode. The two paths from the center electrode being in parallel, the measured conductance is the sum of the conductances of these paths. As the two outer electrodes are short-circuited together, they are at the same potential and no current will flow between them through any external shunt path. Thus, the measurements are independent of external shunt conductances and the cell constant may be determined without the need for duplicating the actual conditions of use. Moreover, this permits the use of a heavy protective metal sheath around the cylindrical cell, eliminating the possibility of shock damage to the plastic tubes which comprise the cell body.

A Wheatstone bridge circuit can be used to measure the conductance of the sea water in the cell, the bridge being energized from an a-c source.

Depth System

The depth of a measuring element can be determined best by measuring the sea pressure at that depth. Pressure is related to depth by considering the average specific gravity of the sea water to be 1.025. Although the actual specific gravity may differ from this value because of temperature and salinity conditions, the integrated error is usually negligible.

Pressure is most simply measured with calibrated spring elements, such as Bourdon tubes or bellows arrangements. The choice between these is governed by the factors; required power output, permanence of calibration, freedom from hys-

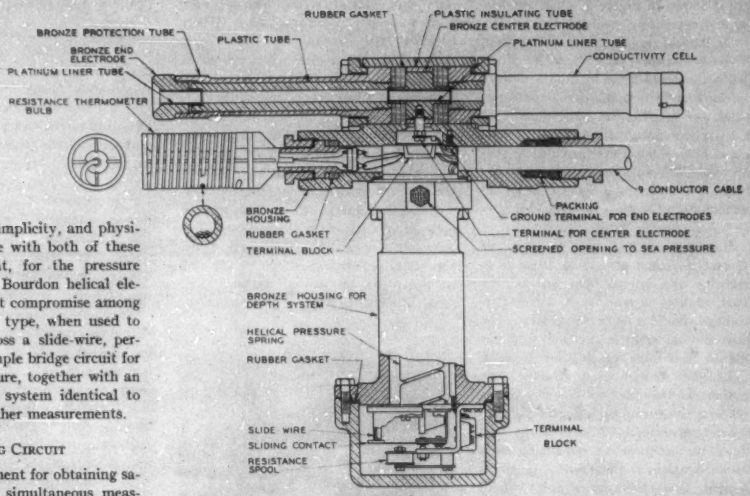


Figure 2. Section view, measuring elements assembly

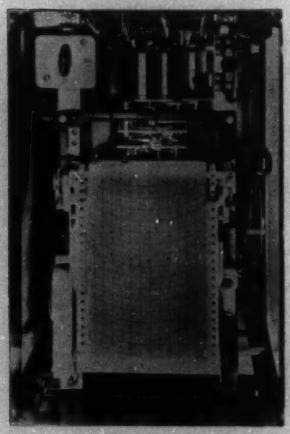
teresis, mechanical simplicity, and physical size. Experience with both of these types indicated that, for the pressure range involved, the Bourdon helical element offered the best compromise among these factors. This type, when used to move a contact across a slide-wire, permits the use of a simple bridge circuit for measuring the pressure, together with an amplifier and servo system identical to those used for the other measurements.

Salinity Computing Circuit

A circuit arrangement for obtaining salinity directly from simultaneous measurements of temperature and conductivity was preferred to the use of a temperature compensating cell as employed in previous instruments.[4,6,8,9] The slow response of a sealed cell designed to withstand the necessary pressure makes it less useful in a hydrographic instrument.

The use of a sealed reference cell in a bridge circuit for determining salinity does not provide exact compensation even when the sample water and the water in the sealed cell are maintained at the same temperature. An examination of the errors shows them to be only slightly less than those for the computing arrangement employed in the present instrument. However, serious errors may result from the difference in temperatures of the water in the measuring and reference cells caused by the thermal lag of the reference cell. Since it was expected that a resistance thermometer bulb could be made with less thermal lag than a sealed cell designed for the same pressure, thus permitting the high-speed performance desired, the computing method was selected.

Figure 3. Recorder, top view



The mathematical functions employed for the computation of salinity were derived from published data[10] relating conductivity to chlorinity and temperature. These data were translated to provide salinity as a function of conductivity and temperature by the use of the relationship

$$\text{Salinity} = 1.805 \times \text{Chlorinity} \qquad (1)$$

in which chlorinity is defined as the total amount of chlorine, bromine, and iodine in grams contained in one kilogram of sea water, assuming that the bromine and iodine had been replaced by chlorine. This expression differs by $0.03^0/_{00}$ from the empirical equation

$$\text{Salinity} = 0.03 + 1.805 \times \text{Chlorinity} \qquad (2)$$

which was established by an International Commission[11] based on samples collected from the Baltic Sea. Since the maximum possible error arising from the use of equation 1 instead of equation 2 amounts to only $0.03^0/_{00}$ and is less than the expected error of the instrument, the employment of the simpler relation is justified. Figure 4 shows the relationships between the specific conductance of sea water and the salinity for the temperature range 30 to 90 degrees Fahrenheit.

When these data are plotted with temperature as the parameter, all of the curves of the family obtained diverge from the origin, and each is slightly concave toward the salinity axis. When the logarithms of salinity are plotted against the logarithms of conductivity for each temperature value, all of the curves are approximately parallel and very nearly straight lines. If they were exactly straight and parallel, it would be possible to represent salinity precisely by the product of two independent functions; one of temperature, and the other of conductivity. By adjusting these curves so that this condition exists, an approximate function of this form is obtained. This complete family of adjusted curves can be represented with reasonable accuracy by the empirical equation

$$S = \left(\frac{100{,}000}{25.661 + 0.73720T} - 348.87\right) c^{1.0946} \qquad (3)$$

where S is the salinity in parts per thousand, T the temperature in degrees Fahrenheit, and c is the specific conductance in mhos per centimeter cube. This particular approximation was selected to give the error distribution indicated in Figure 5. It will be noted that the least errors are incurred for salinities between 30 and $35^0/_{00}$. The average salinity of the surface waters of the oceans is about $35^0/_{00}$. In

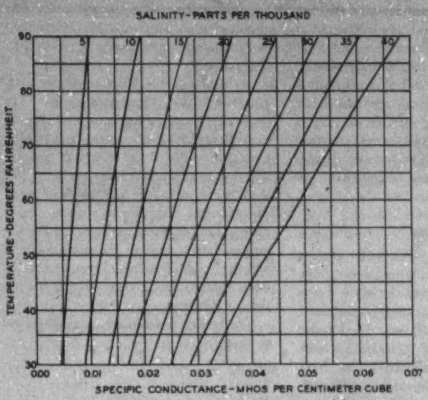


Figure 4. Salinity of sea water from temperature and specific conductance

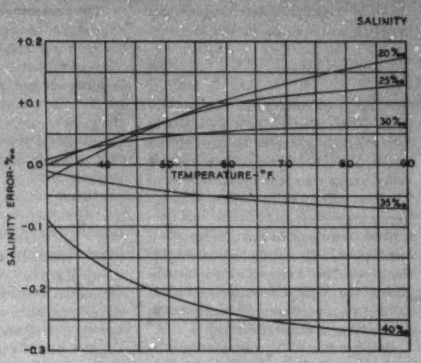


Figure 5. Theoretical salinity error curves

coastal waters it is approximately 32‰. Only in some of the enclosed seas in geographical areas where the evaporation exceeds the precipitation, such as the Mediterranean and Red Seas, does the salinity approach 40‰. Salinities less than 30‰ are encountered in coastal areas near river mouths and in some enclosed seas, as the Black Sea and the Baltic.

In order to perform the computation indicated by equation 3, mechanisms are required for deriving the necessary functions from independent measurements of temperature and conductivity, for multiplying these functions, and for indicating the product by the position of a pen on a chart. Although a much simpler mechanism can be employed for obtaining salinity when a sealed reference cell is used, the computing system was selected as the most practical arrangement chiefly because it makes rapid continuous measurements possible by reducing the time lag to that of the thermometer bulb.

Discussion of Circuits

Amplifiers and Servomotors

In order to provide the power necessary to operate the computing and recording mechanisms the output of each bridge circuit must be amplified. All of the measuring and computing circuits are energized with alternating current of line frequency. The output from each bridge is supplied to an independent electronic amplifier which operates the balancing servomotor of the bridge.

Each amplifier includes a rectifier tube, three stages of resistance-coupled voltage amplification, and a power stage. The tubes are operated below normal ratings to increase their useful life, and ordinary variations in their characteristics have no effect on the performance of the systems. The input resistance of each amplifier is approximately one-quarter megohm, and as the measuring and computing circuits are used in a null-balance manner, large changes in the resistances of the sliding contacts have only small effects on the sensitivities of the measurements. By incorporating a gain control in each amplifier the same type of amplifier serves for both the measuring and computing circuits.

The servomotor employed with each circuit is a small 2-phase induction motor, one phase being energized from the line and the other from the amplifier output. Its direction of rotation depends upon the relative phase of the amplifier output voltage with respect to the line and its speed is a function of the magnitude of this voltage. The slide-wire contacts and their associated recording pens are geared to their respective motor shafts, so that a direct relationship exists between them. The time for full-scale deflection of each of the measured and recorded quantities is approximately five seconds.

Temperature Circuit

Figure 6 is a diagram of the Wheatstone bridge circuit used for the temperature measurement, in which *R6* represents the thermometer bulb, connected to the circuit by three leads, *R1* is a slide-wire, and *R2*, *R3*, *R4* and *R5*, are fixed resistances. Voltage is applied to the bridge from step-down transformer *T1*, and the bridge output is connected to an amplifier. When the bulb temperature changes, the resulting bridge unbalance is amplified to operate a servomotor and move the slide-wire contact to a new position of equilibrium. The position of the contact then represents the temperature at the thermometer bulb.

The calibration equation of the Wheatstone bridge circuit can be derived by setting to zero the expression for its output voltage. Designating e_1 as the voltage applied to the bridge from the transformer *T1*, *P* the resistance of *R1* and *R2* in parallel, *L1* the resistance of each lead wire, and x the resistance of the fraction of *P* between the bottom end of the slide-wire and its contact, then for balance

$$\left(\frac{e_1-i_1L_1}{L_1+P+R_3+R_4}\right)(L_1+R_3+x)-\left(\frac{e_1-i_1L_1}{R_5+R_6+L_1}\right)(R_6+L_1)=0 \quad (4)$$

where i_1 is the current in the transformer branch. Defining

$$R_6=R_{28}+r_1 \quad (5)$$

where R_{28} is the resistance of the thermometer bulb at 28 degrees Fahrenheit and r_1 is the change in the resistance from 28 degrees Fahrenheit to the temperature measured, and by constructing the resistances so that

$$R_3=R_{28} \quad (6)$$

$$\text{and } P+R_4=R_5 \quad (7)$$

equation 4 can be rearranged to give

$$x=\frac{R_5r_1}{R_{28}+r_1+L_1+R_5} \quad (8)$$

As e_1 and i_1 do not appear in this equation the calibration is independent of the applied voltage.

The magnitude of the fractional error

caused by a change of lead resistance can be determined from the derived equation

$$\frac{dx}{x} = -\frac{dL_1}{R_{01}+r_1+L_1+R_5} \tag{9}$$

By making the denominator large compared to the expected dL_1, it is possible to keep small the effects of changes in lead length on the calibration. For the circuit values used a change in lead resistance of 2.5 ohms will cause an error of only 0.1 degree Fahrenheit at full scale, and one-half this error at mid scale. This resistance corresponds to approximately 400 feet of number 18 copper wire. By calibrating the 3-lead circuit for a nominal value of lead resistance, large changes in the lead length can be tolerated.

An expression for the resistance of the thermometer bulb at temperature T is

$$R_8 = R_{01}(1+a(T-28)+b(T-28)^2) \tag{10}$$

where a and b are constants for the wire material. If

$$t = T-28 \tag{11}$$

from equations 5 and 10

$$r_1 = R_{01}(at+bt^2) \tag{12}$$

Substituting 12 in 8

$$x = \frac{R_5 R_{01}(at+bt^2)}{R_{01}(1+at+bt^2)+L_1+R_5} \tag{13}$$

The slope of x with respect to t is

$$\frac{dx}{dt} = \frac{R_5 R_{01}(R_{01}+L_1+R_5)\ (a+2bt)}{[R_{01}(1+at+bt^2)+L_1+R_5]^2} \tag{14}$$

In this equation both the numerator and denominator increase with t, and it is possible by proper selection of R_{01} and R_5 to make the slope practically constant over the full-scale range. The thermometer bulb wire in this instrument is nickel to high purity, and the bridge circuit is designed for this bulb so that the maximum departure of the calibration from a linear scale is only 0.06 degree Fahrenheit.

The measuring sensitivity of the circuit depends on the temperature coefficient of the bulb wire and the current through it. The current used, about 20 milliamperes, is sufficiently low for the bulb construction so that the temperature rise caused by the power dissipated in the winding is negligible. The bulb has a time constant of 0.45 second and consequently gives a response of 90 per cent of the change in one second.

Conductivity Circuit

The conductivity measuring circuit, shown in Figure 7, is a Wheatstone bridge. *R16* is the resistance of the sea water in the cell, *L2* is the resistance of each lead, and *R13*, *R14*, and *R15* are fixed resistances. Transformer *T2* supplies a voltage e_2 to the bridge circuit. The amplifier and servomotor are identical to those used with the temperature measuring system. Designating Q as the resistance of *R11* and *R13* in parallel, and y as the resistance of the fraction of Q between the upper end of the slide-wire and its contact, then, by a treatment similar to that applied to the temperature circuit, it can be shown that

$$y = \left(\frac{Q+R_{14}+L_2}{1+\frac{(R_{14}+L_2)c}{k}}\right)\frac{R_{15}}{k}c \tag{15}$$

where c is the specific conductance of the sea water and k is the cell constant, which are related to *R16* by

$$k = c \times R_{16} \tag{16}$$

The circuit values are selected so that y is equal to Q when c is the full-scale value of specific conductance, c_m.

Since the denominator in equation 15 is not constant, the scale distribution is nonlinear. However, as specific conductance is not directly recorded on the linear chart, but used only for computing salinity, linearity is not necessary. The circuit values used in this instrument cause a maximum nonlinearity of less than two per cent.

As in the temperature measuring circuit, the primary element is connected to its bridge by three leads. Here also the purpose of the 3-lead connection is to keep the errors caused by a change in lead resistance to a small value. If only two leads were used, the sum of their resistances would add directly to the resistance of the sea water in the cell. For the circuit values used in the instrument, a length change of 16 feet of two number 18 wires would introduce an error of 0.1 per cent at full scale.

The bridge resistances are constructed so that

$$Q = R_{13} \tag{17}$$

and

$$R_{14} = \frac{k}{c_m} \tag{18}$$

For these relationships it can be shown that the use of three leads instead of two for connecting the cell to the bridge circuit reduces the maximum error caused by a change in lead resistance in the approximate ratio of eight to one. With the circuit values used in the instrument, a change in lead length of about 125 feet will introduce a 0.1 per cent error.

The constant for a conductivity cell depends on its mechanical dimensions and the sizes, shapes, and surface conditions of the electrodes. Cells of similar design can be readily made having constants within two or three per cent of each other. When a cell is replaced by another having a different constant, the value of the resistance *R15* can be changed to retain the

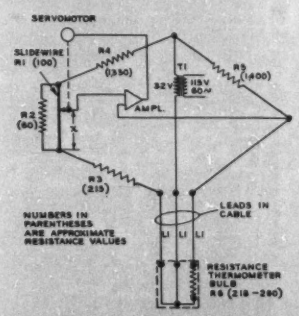


Figure 6. Temperature measuring circuit

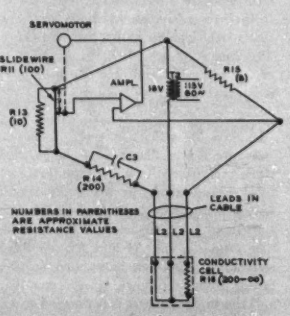


Figure 7. Conductivity measuring circuit

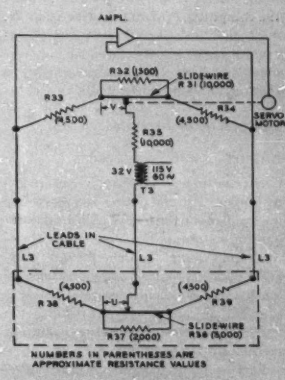


Figure 8. Depth measuring circuit

original calibration for one particular value of specific conductance. For other specific conductances the calibration is altered slightly, the extent of the change depending upon the magnitude of the term $(R_{18}+L_4)/k$ in equation 15. With the circuit components used in the instrument a five per cent change in the value of the cell constant will cause only a 0.08 per cent variation in the calibration curve. Each cell has a separate *R15* resistance spool made for it, and when cells are changed the *R15* spools are also changed.

Another factor affecting the value of the constant of a cell is the phenomenon of polarization, which introduces uncertain errors in the measured conductance. When direct current is passed through a solution, the products of electrolysis accumulate at the electrodes and no constant value of conductance can be determined. With alternating current the ions are not discharged at the electrodes. However, there are still some polarization effects, which are attributed to variations in the ionic concentrations near the electrodes. To obtain good accuracy the effects of polarization must be kept small. This can be accomplished by using a cell with electrodes of large surface area and a large constant and by employing high a-c frequencies. A common method for increasing the effective area of an electrode is the application of a coating of platinum black to its surface. Tests made by Kohlrausch and Holburn[12] indicate that polarization is negligible for platinized electrodes when the area of the electrode in square centimeters is greater than $50\times C$, whereas 0.1 per cent accuracy can be obtained with bright electrodes whose area is greater than $2{,}500\times C$, C being the measured conductance expressed in mhos. Experiments by Thuras[4] have shown that the difference in polarization between 1,000 and 60 cycles is quite small, being in the order of 5 parts in 100,000. Because of its availability, and as it permits the use of the same type amplifier employed with the temperature circuit, 60-cycle alternating current is used for the conductivity circuit.

Another effect of polarization is to produce an apparent high capacitance in series with the conductance of the cell. The reactance of this capacitance is in quadrature with the conductance, and it does not change the measured conductance in proportion to its magnitude. However, it does affect the sensitivity of detecting the balance condition of the circuit and it becomes more important at the lower frequencies. Platinization of the electrodes increases the value of the series capacitance, making its reactance smaller. During the experimental stages of the development of this instrument it was observed that for the cell and electrode arrangement used, the ratios of the series capacitances to the conductances were approximately constant for all salinities within the working range. This ratio is the cotangent of the phase angle between the applied voltage and the resulting current. If the phase angle were exactly constant for a cell it would be possible to cancel completely the quadrature component of the circuit output by introducing the capacitor *C3* across the resistance *R14* to shift the phase of the current in the balancing branch of the circuit an equal amount. This capacitance is determined from the relation

$$C_3=\frac{\tan N}{2\pi f R_{14}} \tag{19}$$

where N is the phase angle of the cell and f is the frequency. By applying a capacitor in this manner the need for platinum-black coatings on the electrodes was avoided.

Depth Measuring Circuit

The depth measuring circuit, shown in Figure 8, is a double slide-wire bridge. *R36* is the slide-wire in the transmitting unit, the contact of which is positioned by the free end of a helical pressure spring. *R37*, *R38*, and *R39* are fixed resistances in the transmitter. *R31* is the slide-wire in the recorder, whose contact is actuated by a servomotor and mechanically connected to the depth recording pen. *R32*, *R33*, *R34*, and *R35* are fixed resistances in the recorder. The resistance of each lead is indicated by *L3*. Transformer *T3* applies a voltage e_3 to the bridge, and the amplifier unit amplifies any unbalance and causes the servomotor to move the recorder contact to restore a condition of balance. In a conventional bridge of this type the amplifier is normally connected between the two sliding contacts, rather than as shown in Figure 8. With a high-input impedance amplifier this arrangement has the advantages of providing uniform sensitivity over the scale range and being unaffected by large changes in the

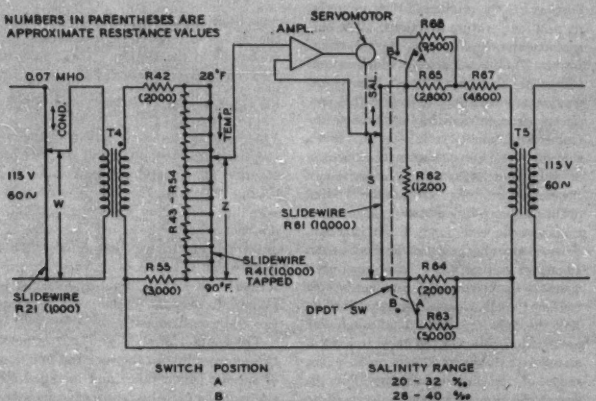


Figure 9. Salinity computing circuit

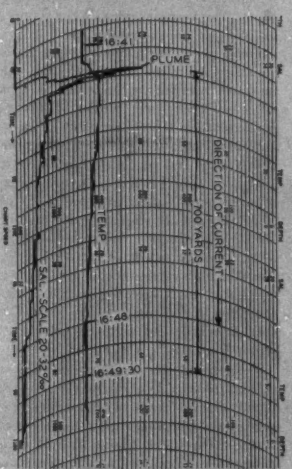


Figure 10. Surface recording in vicinity of sewer outfall at New Bedford, Mass.

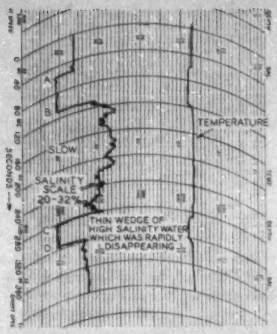


Figure 11. Surface recording in Mount Hope Bay

contact resistances. However, as the lead to the transmitter contact is in the long cable connecting the measuring elements assembly to the recorder, it is subject to inductive effects from the currents and voltages of the adjacent leads connecting to other circuit elements. These can directly introduce errors in the depth measurements, depending upon the amplitude and relative phase of the resultant pickup. To avoid this possibility of error, the voltage supply is connected between the two sliding contacts. Here, the same degree of pickup in the cable lead has a negligible effect because of the relatively large measuring current flowing in it. This is consistent with the connections of the temperature and conductivity circuits, where the amplifier input leads which are sensitive to pickup are all in the on-board equipments, and can be kept short and be properly shielded. This arrangement has the disadvantage of causing variable measuring sensitivity over the depth scale and less protection against variations in the contact resistances. However, by making the resistance of *R35* high, and the effective resistances of the slide-wires low, in relation to the resistances on each side of the slide-wires, these disadvantages become negligible. By doing this there is considerable attenuation of the useful power, but as the total power supplied to the bridge is small this is not a serious matter.

Designating the parallel resistance of *R31* and *R32* by *M*, the parallel resistance of *R36* and *R37* by *N*, with *v* and *u* representing the fractions of *M* and *N* respectively, from the left-hand end of each slide-wire to its contact, and by constructing the resistances so that

$$M = N \tag{20}$$

and

$$R_{33} = R_{34} = R_{38} = R_{39} \tag{21}$$

then the contact position at balance is given by

$$v = \frac{(2R_{33} + M)u + ML_5}{2(R_{33} + L_5) + M} \tag{22}$$

For this circuit arrangement, the maximum error caused by a change in lead resistance can be shown to be represented by

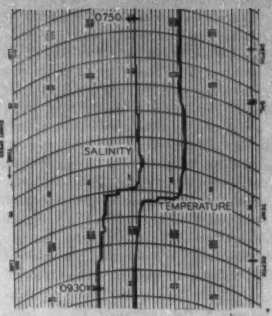
$$\frac{dv}{M} = \frac{dL_5}{2R_{33} + M} \tag{23}$$

Thus, by making the denominator large, the effects of changes in the lead resistance will be small. In the instrument this value is 10,000 ohms, so that a 1-ohm change in lead resistance (160 feet of number 18 wire) will cause a maximum error of only 0.01 per cent.

The depth elements for the different ranges are made interchangeable by setting the fixed resistances associated with each element in accordance with the relations indicated in equations 20 and 21.

Salinity Computing Circuit

Figure 9 is a diagram of the circuit used for computing salinity from the simultaneous measurements of conductivity and temperature. Slide-wire *R21* is connected across the line, and its movable contact is positioned from the shaft of the conductivity unit according to a specific function of conductivity. The current in the temperature compensating slide-wire *R41*, connected to the secondary of transformer *T4*, is proportional to this same function, and its contact is positioned by the shaft of the temperature unit. The output voltage of *R41* is, therefore, proportional to the product of the conductivity and temperature functions. The current in slide-wire *R61* is supplied by transformer *T5*, the primary of which is connected to the power line. The transformers are used only to isolate the computing circuits from the line, each transformer having unity voltage ratio. When the voltage is zero between the contacts on slide-wires *R41* and *R61* the position of the contact on *R61* represents the product of the conductivity and temperature functions given in equation 3, which is the salinity. Any change in this product causes an output voltage, which is amplified, and operates the servomotor to move the *R61* contact to a new position of equilibrium. *R42* through *R55* and *R62* through *R67* are fixed resistances. The switch associated with slide-wire *R61* serves to set the range of the salinity circuit. For the switch position shown the range is 20 to



S.T.D. NO. 2 – 24 APRIL 1947 – CELL AT 3½ FT
TIME: 0750–0930
LAT.: 39°26.1 N – LONG.: 69°55.1 W
LAT.: 39°16.6 N – LONG.: 69°52.7 W
SALINITY SCALE 28–40‰

Figure 12. Surface traverse across edge of Gulf Stream

32‰. The amplifier and servomotor are identical to those used with the other measuring systems.

Designating the resistance of *R41* paralleled by the series resistances *R43* through *R54* by *H*, and the parallel resistance of *R61* and *R62* by *J*, with *w*, *z*, and *s* representing the effective variable resistances, as shown in Figure 10, then it can be shown that the amplifier input is zero when the three contact positions, *w*, *z*, and *s*, are related so that

$$s = \left(R_{42} + R_{43} + J + \frac{R_{44}R_{45}}{R_{43} + R_{44}}\right) \times \left(\frac{w}{R_{21}(R_{42} + H + R_{55}) + w(R_{21} - w)}\right)(R_{55} + z) - \frac{R_{55}R_{54}}{R_{55} + R_{54}} \tag{24}$$

As voltage does not appear in equation 24 the balance position is unaffected by variations in the supply. This equation for *s* can be expressed in simplified form by

$$s = Af_1(w)f_2(z) - B_1 \tag{25}$$

Where *f* indicates the functions of the respective variables and *A* and *B1* are constants. The function of *w* includes the loading effect of the temperature-compensating circuit on slide-wire *R21*, but does not consider the added load of the exciting current of transformer *T4*. This load, which is kept low by using a transformer with a large number of turns, is taken into account in the initial calibration of the instrument.

The contact of slide-wire *R21* is positioned according to the required function of specific conductance and modified for the loading effects by means of a cam asso-

ciated with the output shaft of the conductivity unit. The contact on slide-wire *R41* is moved directly by the output shaft of the temperature unit, and its position is linearly related to temperature. The required function of temperature is obtained by using the proper distribution of effective resistance in the slide-wire. This is accomplished by tapping the linear slide-wire winding at several points and shunting each section separately with a fixed resistance. This method approximates the continuous curve by a series of straight-line sections, the approximation becoming better as more sections are employed. In the instrument 12 sections are used in slide-wire *R41*, which together with the associated shunting resistances, *R43* through *R54*, provide an approximation with a maximum error of 0.05 per cent.

Large changes in the resistances of the contacts on slide-wires *R41* and *R61* are negligible, as they are in series with the high resistance input of the amplifier. A change in the resistance of the contact on slide-wire *R21*, however, will affect the accuracy of the computation because it alters the voltage applied to transformer *T4*. The error resulting from this condition can be minimized by making the equivalent impedance of the transformer and its load large compared to the resistance of *R21*. No difficulty has been encountered in practice with this arrangement.

The first step in the design of the computing circuit is the selection of a conversion factor relating salinity units to resistance or voltage units. Although this selection is made arbitrarily, the choice of the factor is restricted by consideration of circuit output sensitivity, applied voltage, and the physical properties of the slide-wires. The factor used in the instrument is 80 ohms per part per thousand of salinity.

The salinity range of the chart is determined by the position of the double-pole double-throw switch. Thus, for the position shown, the *B1* constant in equation 25 is equivalent to $20^0/_{00}$. When the switch is in the high-range position

$$B_1 = R_{62} \tag{26}$$

which is equivalent to $28^0/_{00}$. The *A* constant is then

$$A = R_{67} + \frac{R_{62}R_{63}}{R_{62}+R_{63}} + J + R_{64} \tag{27}$$

The resistances are proportioned so that

$$R_{62} + \frac{R_{62}R_{64}}{R_{62}+R_{64}} = \frac{R_{63}R_{65}}{R_{63}+R_{65}} + R_{64} \tag{28}$$

Thus, the constant *A* has the same value for both switch positions, maintaining the same current through slide-wire *R61* for each of the salinity ranges.

The parallel arrangement of resistances is used so that the switch contacts will be in series with the high resistance shunts, thus making negligible the effects of varying switch contact resistances.

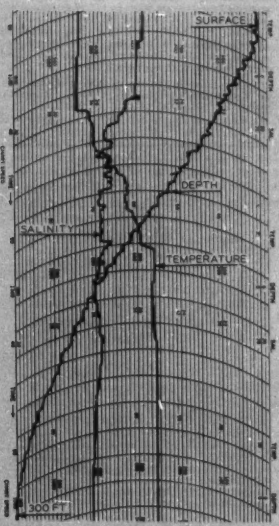
Use at Sea

Several of the salinity-temperature-depth recorders have been made and are being used regularly at sea. An earlier model, which recorded salinity and temperature only, has been in use for nearly three years. These instruments have proved to be well adapted to securing continuous measurements from a moving vessel. For determining surface water conditions, the measuring elements assembly may be mounted on the hull below the water line, where the conductivity cell is irrigated by the boat's motion, or inboard in which case it is irrigated by a stream of water maintained with a pump. When used on a small boat, where the intake is within a few feet of the surface, air bubbles are frequently drawn into the circulation and cause undesirable fluctuations in the salinity record. This difficulty did not appear when the measuring elements assembly was installed on an intake line of the research vessel Atlantis which was located 12 feet below the water line. On small boats it may be overcome by mounting the intake tube on a strut which holds it about one foot away from the hull at a depth of two or three feet.

Figures 10 and 11 are illustrations which show how strikingly the instrument records sudden changes in the temperature and salinity of the surface water. In Figure 10 a record is shown taken from a boat which passed directly over the outfall of the sewer at New Bedford, Mass., in the direction in which the tide was flowing. Note the sharp discontinuity encountered upstream of the outfall, the great drop in salinity as the plume was crossed and the gradual increase in salinity which occurred over a distance of 700 yards downstream. Figure 11 shows a sharp discontinuity in salinity encountered in Mount Hope Bay where a tongue of more saline water had become surrounded by dilute water flowing from the Taunton River. The boat entered the saline water at *A*, crossed into more dilute water at *B*, turned and re-entered the tongue at *C* and emerged from it at *D*.

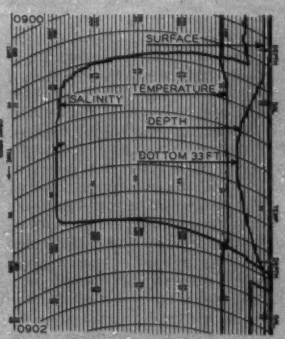
Figure 12 shows a record of the temperature and salinity at a depth of three and one half feet made in a 10-mile traverse across the inshore margin of the Gulf Stream.

Figures 13 and 14 show records of vertical measurements through temperature and salinity gradients. The depth transmitting element used for these surveys was calibrated to give full-scale deflection



LAT.: 39°45'N — LONG.: 70°45'W
AUG. 7, 1947 – BALANUS CRUISE NO. 10
S.T.D. NO. 1 – DEPTH SCALE 0-300'
SALINITY SCALE 28-40 ‰

Figure 13. Vertical recording in ocean water



THAMES RIVER, NEW LONDON, CONN.
S.T.D. NO. 2-3 APRIL, 1947 — 0900 — 0902
DEPTH SCALE 0-300 FT
SALINITY SCALE 20-32 ‰

Figure 14. Vertical recording in Thames River Estuary at New London, Conn.

of the depth recording pen for 300 feet. The true depths are obtained from the records by dividing by two the readings on the 0–600 scale. These records indicate clearly the usefulness of the instrument for obtaining the hydrography of complicated water structures. It can be noted from Figure 13 that the temperature and salinity are reasonably constant from the surface to a depth of 50 feet, indicating a well-mixed layer. From 50 to 200 feet both the temperature and salinity undergo large changes, but the gradients are not uniform. Below 200 feet both quantities are reasonably constant. The considerable wiggle in the depth trace was caused by the rolling of the vessel. Note how the magnitude of the wiggle is reduced with increasing depth, as the sag in the lengthening cable absorbs the vertical motion of the boat.

Figure 14 shows the very sharp salinity gradient in the estuary of the Thames River at New London, Conn. A shallow layer of fresh water from the river overlays the more saline water from the Long Island Sound. From both the large positive salinity gradient and the negative temperature gradient it is evident that a large positive density gradient exists, indicating a very stable water column with little tendency for vertical mixing.

Experience has indicated the need for taking certain precautions in using the instrument. It was anticipated that serious errors in the measurements of conductivity might result from fouling of the cell with bacterial slime of macro-organisms. A cell has been operated under the wharf at Woods Hole for weeks at a time. Under these conditions the cell constant slowly changes, causing a salinity error of as much as 0.5‰. Under active use during surveys difficulties due to fouling have not arisen, but it is clear that the measuring elements should not be mounted permanently where they are inaccessible to cleaning or are subject to fouling over prolonged periods. Some difficulties caused by inadequate flushing of the conductivity cell have been encountered in vertical surveys, making it necessary to exercise care in controlling the rate of descent of the measuring elements. Further study is being given the cell design with the object of improving its flow characteristic. Improved accuracy over limited ranges of the measured quantities can be obtained by taking calibration samples of water within the ranges and applying fixed corrections to the records.

Conclusions

A practical salinity and temperature recording instrument is described. This type of instrument has been operated regularly for more than three years and has been found useful for obtaining hydrographic data. It has been applied particularly for inshore surveys in shoal water and in ocean areas where its accuracy of 0.2 degree Fahrenheit and 0.3‰ is adequate. Its time constant of one-half second permits continuous records of temperature and salinity to be made from a moving vessel, or while the measuring elements are being lowered vertically.

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No Discussion

A New Line of Thyratrons

A. W. COOLIDGE, Jr.
ASSOCIATE AIEE

FOR several years one important application for thyratrons has been their use in controlling d-c motors operated from a-c lines.[1-3] A thyratron acts as a switch, having rectifying properties, and is capable of applying all or any fractional part of the positive half of the a-c cycle to its load. In this way the average voltage applied to a d-c motor may be varied. Precise motor control is obtained by varying the operating angle of thyratrons in either the field or armature circuits or in both circuits simultaneously. Both gas-filled and mercury-vapor thyratrons have been employed successfully with certain limitations for the two types as described in the following:

GAS-FILLED

1. In general, tubes have had low-anode voltage ratings in the order of 500 to 1,000 volts.

2. Tubes have suffered from gas clean-up (or reduction of the gas pressure) causing the control characteristic to shift beyond specified limits, or the tube drop to become excessive, either of which renders the tube inoperable.

MERCURY-VAPOR

1. Restricted ambient temperature range has required either forced draft cooling in compact panels or conversely larger panels in which there is only natural draft available.

2. A cathode preheat period is very often necessary to raise the mercury temperature to within the operating temperature range before anode voltage may be applied.

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A. W. COOLIDGE, JR., is with the electronic tube department of the General Electric Company, Schenectady, N. Y.

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To meet requirements of the applications, designers of electronic motor-control systems have undertaken to reduce costs and size of the equipment by eliminating many features heretofore deemed necessary to overcome the limitations for mercury and gas-filled tubes.

Tube Specifications

Such a streamlining program called for the development of gas-filled thyratrons having the following characteristics:

1. Maximum cathode heating time of one minute.
2. Small size.
3. Ambient temperature range of minus fifty-five to plus seventy degrees centigrade.
4. Peak to average current ratio of 12:1.
5. Peak forward and inverse anode voltage rating of 1,500 volts.
6. Resistance to vibration and shock.
7. Low cathode power.

The first tube to be developed was the *GL-5545*. This tube has a cathode power of 50 watts and average and peak anode currents of 6.4 and 80 amperes respectively. The over-all size is $2^5/_8$ inches in diameter by 8 inches in length.

Design Theory

Short heating time dictated a filamentary type cathode with little heat shielding, while the high peak current demanded a tenacious cathode coating with the ability to withstand severe ion bombardment. Conventional barium oxide coatings such as are used in receiving tubes and most thyratrons are generally limited in emission to the equivalent of one ampere maximum per square centimeter of emitting surface. Current densities in excess of this value tend to destroy the coating because of excessive ion bombardment. A superior coating which will withstand current densities of three amperes per square centimeter of emitting area may be realized by adding nickel sesquioxide (Ni_2O_3) in a finely divided form to the ingredients of barium oxide coatings. The Ni_2O_3 acts as an agent in fusing the barium oxide to the base metal producing a strong metallic coating as contrasted with the comparatively fragile and powdery conventional coating.

The high conductivity of the resultant metallic coating causes the emission to be well distributed over the surface of the filament and reduces local heating as current travels from the base metal to the surface of the coating.

The attack on the problem of gas clean-up can best be understood by first considering its cause in a thyratron.

During tube conduction, ions positive in potential with respect to the cathode are continuously attracted to the cathode. Some ions gain sufficient velocity to become imbedded in the cathode coating, thereby reducing the total number of free gas molecules and hence reducing the gas pressure in the tube. Normally the tube drop is quite small, however, so that gas clean-up from this action alone is not serious.

By far the greatest clean-up occurs when the anode voltage assumes a high negative value in the presence of ions immediately after conduction. Ions at this time impinge on the anode with sufficiently high velocities to penetrate the surface of the metal and become permanently trapped, and the process continues until the metal directly below the surface becomes saturated. If ion bombardment is particularly severe, material may be sputtered from the anode surface, the resultant sputtered film containing entrapped gas.[4] Thus, during sputtering, a new anode surface is continually made available for further gas absorption. If the gas pressure drops below a value of a few microns, tube failure occurs.

The actual rate and amount of gas clean-up may be a function of both the degree of ionization in the tube at the time of current cessation and the rate at which the inverse anode voltage is ap-

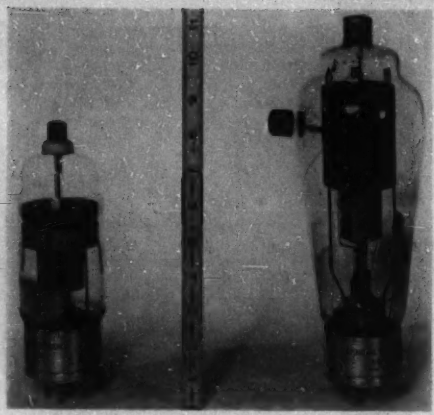


Figure 1. Size contrast between gas-filled GL-5545 and mercury-filled FG-105

Both tubes have an average current rating of 6.4 amperes

plied. Since ions have inertia, the number of ions in a tube at a particular time will be a function of the current through the tube a few microseconds previously. Therefore, the degree of ionization at the time of current cessation is a function of the rate of decay of anode current just prior to this time. Again, because of ion inertia, a finite time is required to deionize the tube, hence the rate of rise of inverse anode voltage determines the severity with which residual ions impinge on the anode. Therefore, when considering the effect that a circuit may have on a gas-filled thyratron, it is convenient to use, as a measure of severity, the product of the rate of current decay in amperes per microsecond prior to commutation, and the rate of rise of inverse voltage in volts per microsecond just after commutation.

If two thyratrons are used in a biphase half-wave rectifier circuit with an inductive load as illustrated in Figure 2, conditions favorable for gas clean-up are realized when the thyratrons are phased to provide continuous load current. The solid line *ABCDEG* represents the voltage across one tube. The counter electromotive force of the inductance maintains a positive anode voltage and forces the tube to conduct long after the supply voltage has gone negative, until at the time of extinction, the voltage changes abruptly to a high negative value. The voltage across the other tube is identical but shifted 180 degrees in phase. The fact that load current is continuous means that both tubes alternately conduct essentially square blocks of current, and that the rate of current decay for the trailing tube during commutation is very high, limited only by the leakage reactance of the transformer.

Considerable field experience with available gas-filled thyratrons of conventional design has clearly demonstrated the fact that circuits which apply inverse voltage abruptly after a high rate of current decay do cause rapid clean-up.

Circuit cushioning or snubbing, in which a series resistor and capacitor are connected across each thyratron to retard the rate with which inverse voltage is applied, as along *ABCDEFG*, Figure 2, has been used with varying degrees of success. At best, the added components represent an increased cost to the panel and allowance must be made for dissipating considerable power loss in the cushion components.

From the standpoint of tube design, it was felt that improvements could be made over the conventional thyratron design whereby the problem of gas clean-up could be minimized. By conventional thyratron design is meant a design, Figure 3, in which a disk-shaped or cup-shaped anode is mounted on the end of a lead sealed to the top of the bulb; the grid and cathode are mounted from the cathode stem leaving a grid-anode separation of from one-quarter to three-quarters of an inch.

One attack on the clean-up problem might be merely to increase the gas pressure in a conventional design thyratron several fold on the basis that saturation would certainly occur before the gas pressure was reduced to the point where tube characteristics were impaired. A significant law by Paschen (Figure 4), however shows that there is a definite relation between the product of gas or vapor pressure p and effective electrode spacing d and the maximum potential that may be applied between electrodes without causing ionization or breakdown.*,[8]

For a thyratron, this means that a large increase in gas pressure would reduce the maximum controllable anode voltage. Anode voltages in excess of those governed by the law would cause ionization from anode to grid or around the outside of the grid to the cathode shielding, the cold discharge producing enough ions to cause the thyratron to pick up or conduct in normal fashion. A further complication results from the fact that the d in Paschen's law is not necessarily the direct spacing between the anode and the nearest electrode. The actual effective distance d may be equal to the length of a curved path, crossing equal potential lines at right angles, between any part of the anode to any part of the other elements. Hence, it is the "long-path" discharge that limits the maximum controllable anode voltage of a thyratron. The actual length of a "long-path" is vague and influenced by such irregular factors as the point of origin, the shape of the bulb and internal tube structure, nature of anode surface, and constriction along the path. For this reason it is impossible to calculate the absolute maximum controllable anode voltage for a given pressure and tube geometry.

While Paschen's law is not generally helpful for making absolute calculations it is invaluable in the relative sense.

Several types of gas tubes of conventional design with an anode voltage rating of 500 volts are available. These are filled with about 100 microns of gas initially. The voltage rating is too low for many motor control applications. Other gas tubes commonly employed in motor control and having voltage ratings of 750–1,000 volts have been initially loaded with about 50 microns of gas. It is apparent that if a conventional design were to be used at the desired 1,500 volt rating the initial gas pressure would have to be so low that any gas clean-up could not be tolerated.

Paschen's law indicates that in order to establish a 1,500 volt anode voltage rating and still assure loading the tube with the maximum amount of gas, the electrode spacing should be made as small as possible and the anode completely shielded, except for an opening to the control grid, to prevent spurious long-path discharge.

* At present, there is no known data for Paschen's law in terms of Xenon. However, it is believed that a curve for Xenon would have the same general shape as that for mercury.

Tube Construction

In order to make possible this close electrode spacing, a method of stack mounting the tube elements and spacing them by means of thin insulators was devised. Three equally spaced support rods welded to cathode stem leads and carrying tubular insulators, concentrically position all the parts and insulate them from one another. Referring to the cross section (Figure 5) the elements, starting with the first cup above the cathode, are shield, control grid, shield, anode, and shield. All cups below the anode have a circular opening at the center with the exception of the control grid. Several narrow rectangular openings have been punched out of the center of the control grid cup to provide the tube with a steep control characteristic. The control grid and shields are electrically connected to two different support rods. The grid connection is brought out through the base while the shield connection is made to the midpoint of a center tapped resistor connected across the filament leads inside the base. In this way the shields maintain the same potential as the anode and grid return which is also at the electrical center of the filament circuit by virtue of its connection to the center tap of the filament transformer.

Though the number of parts in the stack is large, the parts are mounted together easily and care must be taken only to position correctly the bottom eyelets on the three support rods and place the parts in their proper sequence on the stack. The relation of the length of the tubular insulators to the height of the stack is such that the top surface of the top bead insulators will always be slightly higher than the top of the tubular insulator. Thus when the top eyelets are pressed down and welded in place any slack is taken up and the whole assembly becomes a rigid structure. Since the anode is an integral member of the stack, the grid-anode structure becomes anchored to the top of the bulb when the anode seal is made. Small sputter cups which enclose and shield the bead insulators are located at strategic points in the stack where it is necessary to guard against electrical leakage paths which would be formed on the insulators by deposition of evaporated cathode materials.

The fact that the tube structure, with the exception of the light cathode, is supported from both ends of the bulb provides a rigid assembly which withstands high accelerations in vibration and shock.

It can be seen from Figure 5 that the top of the anode and the anode lead are well shielded and closed off from the top portion of the tube so that a long-path discharge cannot occur between some point at high anode potential and another point at low potential. Were it not for the shield over the anode, ionization could strike over a looped path centimeters in length between the top of the anode and the top of one of the support rods at or near cathode potential. The effective electrode spacing d then would be centimeters in length. Such a discharge would be more likely to occur than an even longer one from the top of the anode to the control grid or cathode because of the constriction between the bulb and the shield at the side of the anode.

Looking at the bottom anode surface, one sees that the longest effective path d between anode and any low potential element corresponds to the distance from the center of the anode to the center of the control grid, and that this d is millimeters in length. It is apparent that this latter value of d is the largest and the governing one in the *GL-5545* and may be considered as the effective electrode spacing between the anode and a low-potential element (in this case the control grid). The fact that the effective electrode spacing d in the *GL-5545* may be measured in millimeters rather than in centimeters, as is the case with conventional type thyratrons, permits the loading pressure p to be as high as 100 microns and the product pd still small enough to permit a 1,500 volt anode voltage rating.

In addition to the advantage of the high-gas pressure contained in the *GL-5545*, the thorough shielding of the anode and small electrode spacing are further instrumental in reducing gas clean-up. After current commutation, when the anode voltage may be highly negative, and there is still residual ionization in the tube, the ions bombarding the anode are drawn from the small column of ions beneath the anode. The height of this column is equal to the distance between anode and control grid and its diameter is equal to the diameter of the opening in the shield directly below the anode. For a given current the total number of ions in the column is a function of the distance between the grid and anode, and therefore the small grid-anode spacing holds ion bombardment of the anode to a minimum. Ions in other regions of the tube collect on the surfaces at cathode potential, on the control grid, or on the bulb walls. By contrast, all of the anode surface in the conventional design, Figure 3, is available for ion bombardment, while the larger grid-anode spacing results in more ions being available for bombardment.

Because the *GL-5545* is a shielded grid

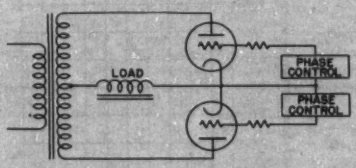


Figure 2 (A). Biphase half-wave rectifier circuit

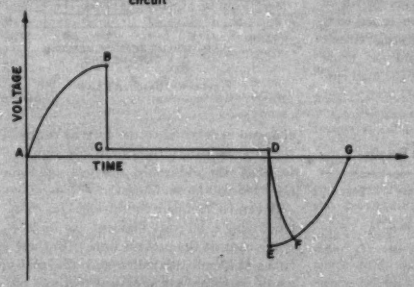


Figure 2 (B). Voltage across one tube

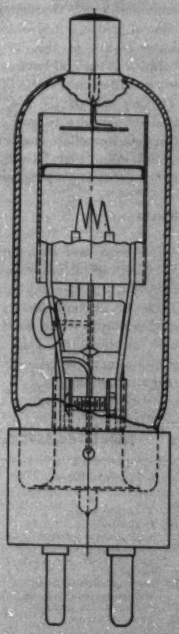


Figure 3 (right). Tube FG-178-A

triode, it possesses some of the advantages associated with dual-grid thyratrons such as low grid-to-anode capacitance and high grid-to-cathode capacitance. This capacitance relation offers increased tube stability in that transients or disturbances in the anode voltage supply are less likely to be coupled to the grid in sufficient magnitude to cause spurious triggering of the tube. Another phenomenon known as constriction is less likely to cause damage to grid circuits when a shielded-grid thyratron is used. Constriction occurs only in tubes in which there is low gas or vapor pressure resulting in ion starvation and the inability of the tube to carry high peak current. When constriction occurs, the current through the tube ceases abruptly despite the fact that the anode supply voltage may be highly positive.[8] The anode voltage will not only build up to the supply voltage, but because of the rapid rate of change of current through the transformer, the anode voltage may assume a value many times the actual supply voltage. In the event of constriction, the maximum voltage appearing at the grid is $C_{pa}E_b/C_{pa}+C_{gk}$, where C_{pa} is grid-anode capacitance, C_{gk} is grid cathode capacitance, and E_b is the peak voltage appearing at the anode. In circuits employing thyratrons in which the control grid is not shielded, high-voltage wave fronts of this type capacitively coupled to the grid have caused failures of low-voltage components in the grid circuit. The fact that the *GL-5545* ratio of anode-grid capacitance to grid-cathode capacitance is low offers maximum protection against component failures in grid circuits in the event of constriction.

Tests

Life tests have been conducted on *GL-5545*'s in typical motor controls under two different sets of conditions as shown in Table I. In both tests two tubes were used in a biphase half-wave rectifier circuit with an inductive load, and the firing angles were adjusted so that the load current was continuous.

Table I. GL-5545 Test Results

	Per Tube Values	Test 1	Test 2
A.	Average current	2.5	4.75
B.	Peak current	5	9.5
C.	Peak inverse voltage	1,000	1,000
D.	Rate of rise of inverse voltage in volts per microsecond	10	5
E.	Rate of current decay in amperes per microsecond	0.15	0.15
F.	Product of (D) and (E)	1.5	7.5–15
G.	Tube life hours	4,000	3,500

In the first test, the rate of rise of inverse voltage was conditioned by cushioning circuits. In the second test, cushioning circuits were eliminated, and the rate of rise of inverse voltage was extremely high.

For comparison, the same tests were repeated on established gas tubes of older design which were known to have long life in circuits presenting resistive loads. The tube ratings were such that the maximum permissible product of rate of rise of inverse voltage and rate of current decay (in volts and amperes per microsecond respectively) was 0.66. Tests 1 and 2 then yielded tube lives of 300 and 250 hours respectively.

Further tests are being conducted to determine the maximum product of rate of rise of inverse voltage and rate of current decay that the *GL-5545* will tolerate. It appears that little cushioning if any will be needed for motor circuits.

Other Tubes in Line

Present plans call for the development of other gas-filled tubes suitable for motor-control work ranging in average current ratings from 1/2 ampere to 12.5 amperes. Although the physical tube size will be

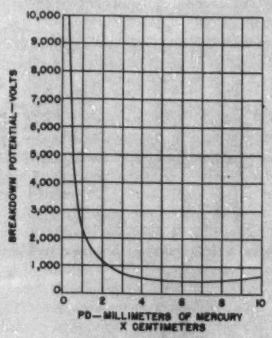


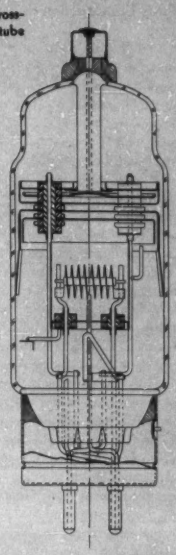
Figure 4. Paschen's Law

changed in relation to the current rating, the internal tube structure will be similar so that the desirable features of the *GL-5545* will be retained.

Several 3.2 ampere thyratrons, *GL-5544* employing a 30-watt filament are undergoing tests at the present time. In these tubes the bulb diameter and the grid-anode structure are identical to those of the *GL-5545* and the height of the bulb has been reduced one inch.

Gas-and-mercury combination tubes

Figure 5. Cross-section of tube GL-5545



are those which contain a charge of both mercury and gas. These tubes will conduct on the charge of gas during warm-up periods, but as the mercury temperature is increased, the mercury takes over and carries the load. Hence, combination tubes combine the advantages of mercury-filled tubes with the quick heating associated with gas-filled tubes. Because the summation of warm-up periods in a tube is a small fraction of tube life, only a small charge of gas is needed. Low-gas pressures of 50 or fewer microns have permitted anode voltage ratings in excess of 1,000 volts for conventional design thyratrons.

Gas-and-mercury combination tubes have been made in the 3.2 ampere rating. The electrode structure is identical to that of the 3.2 ampere gas tube, but the structure has been placed in the taller *GL-5545* bulb and a heat shield added below the filament. The larger ratio of bulb area to watts dissipated and the heat shield allow the condensed mercury temperature to fall within acceptable limits.

Combination tubes, however, are limited as are mercury tubes in the maximum ambient temperature rating and must be larger in size than gas-filled tubes having the same current rating. In the light of operating data for the gas tubes of this new design, it would appear that there is little to be gained by the use of combination gas-and-mercury tubes.

Conclusion

Tests have established the value of combatting gas clean-up and extending anode voltage ratings in gas-filled thyratrons by employing a design in which the elements are closely spaced and the anode well shielded. Excessive clean-up is eliminated by a combination of the following factors:

1. The tube is filled with a generous gas supply.
2. Minimum area of the anode is exposed to ion bombardment.
3. Ion population is small.

Results of tests to date indicate that tubes of the new design will operate with freedom from many of the limitations of older design gas-filled thyratrons.

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Discussion

Hubert H. Wittenberg (Radio Corporation of America, Lancaster, Pa.): Although the *GL-5545* is rated at 6.4 amperes average and 80 amperes peak, life test results are given only for 4.75 amperes average and 9.5 amperes peak. What life is obtained at full rated peak and average currents? Gas clean-up can occur during the ionization period as well as after the conduction cycle. Witness the uses of gas thyratrons in radar service and in sweep oscillators where the inverse voltage is negligible.

At what temperature does the anode of the *GL-5545* operate? The enclosed structure should result in a high anode temperature. The low gas clean-up may result from the high-anode temperature as well as the restricted anode area available for ion bombardment.

Has grid emission been a problem in the development of the *GL-5545*? What is the grid emission in terms of shift of the control characteristic with small and large grid resistors at rated average anode current?

Why is the shield grid connected to the center tap of a resistor in the base of the *GL-5545*?

D. V. Edwards (Electrons, Incorporated, Newark, N. J.): Having had experience with a line of tubes similar in ratings and cathode coatings to those discussed by Coolidge, I can confirm his conclusions that much improvement can be made in the commutation factor rating and voltage rating of gas filled tubes by modifications of the tube design and that commutation ratings of latest designs are approaching the point where cushioning is no longer required.

Our experience would justify going beyond the author's statement in regard to gas clean-up. In the two years since commutation ratings for gas-filled tubes were proposed, gas clean-up has been eliminated completely where the commutating duty imposed by the circuit was within tube rating.

In comparing tube designs, we have not been able to get reliable information from accelerated life tests by operating tubes at above rated commutation duty and comparing the lives obtained. The reason for this becomes apparent when the lives obtained within a particular tube design are plotted against the commutating duty applied by a series of circuits of increasing duty.

This curve has such a sharp knee that individual tube variations mask the effect of variation in tube design when tests are run in the region beyond correct commutation rating.

The interpretation of the author's data on the type *GL-5545* given in Table I of the paper would be facilitated by knowing how failure occurred in test 1 and whether detectable gas pressure change had occurred.

The author attaches more importance to the effect of loading pressure as a means of avoiding gas clean-up than experience with type *C6J* would indicate. This tube, a 1937 design, carries a loading pressure higher than any of the tubes listed in the paper, yet has a slightly lower rated commutation factor whereas the *C6M* with the same loading pressure as the *C6J* has a higher commutation factor.

A. W. Coolidge, Jr.: Wittenberg's questions are answered below in the order in which they were presented in the discussion.

The average life of the *GL-5545* when operated at 6.4 amperes average and 80 amperes peak is 3,000 hours. The writer concurs that clean-up can occur during the ionization period as well as after the conduction period. Tests indicate, however, that clean-up during conduction is negligible compared to that which occurs after conduction when the product of the rate of rise of inverse voltage and the rate of current decay is high.

When the *GL-5545* is operating at full load the anode temperature is about 900 degrees centigrade. The high anode temperature is not a cause for the low gas clean-up since the tubes of conventional design which were run under the conditions of Table I and which suffered short life because of gas clean-up operated with anode temperatures only slightly lower than those of the *GL-5545*.

Grid emission was a serious problem in the development of the *GL-5545* until the cathode coating using the nickel sesquioxide was substituted for the conventional barium oxide coating. As mentioned in the paper the nickelplate coating is metallic, tenacious, and well bonded to the base metal, and hence there is less tendency for the coating to be sputtered and deposited on the control grid. The maximum grid emission current in a *GL-5545* during normal operation should not exceed ten microamperes.

The shield grid is connected to the center tap of a resistor in the base so that it is held at the potential of the anode return. Thus, the tube has the external connections of a triode and the shield grid does not cause spurious shifts in the control characteristic as it might were it left floating.

At the outset, the writer wishes to clarify a term in Edwards' discussion which was not introduced in the paper. The term, "commutation factor" has been applied to the product of the rate of rise of inverse voltage in volts per microsecond and the rate of current decay in amperes per microsecond. Its usage is gaining favor, and it is a convenient term in the discussion of gas cleanup.

With reference to the second paragraph of Edwards' discussion, the author concurs that gas clean-up has been sufficiently reduced by tube or circuit design so that it is no longer a factor in the life of the tube when the tube is used within the rated commutation factor. By the use of a snubber circuit as described in the paper the commutation factor can be limited to less than the maximum rated commutation factor for any tube. The snubber components do, however, represent added cost, require extra space, and perhaps of more importance there is a continuous power loss during operation, the size of the loss being dependent on the amount of snubbing required. The *GL-5545* described in the paper has a rated commutation factor more than ten times higher than that for a tube of conventional design and of otherwise equal rating. This means that the *GL-5545* requires much less snubbing than tubes of conventional design.

Edwards has found a sharp knee in the curve of tube life versus commutation factor for a particular design. The tests on the tubes of conventional design described in the paper would appear to support this as it is known that these tubes have long lives when the commutation factor is below 0.66. Yet, when the commutation factor was increased to 1.5 in test 1 and about 10 in test 2, the tube life was only 300 and 250 hours respectively.

Another test which has been run since the paper was written and in which the commutation factor was 40 yielded a tube life of 300 hours. This would indicate that there is a critical threshold value for the commutation factor below which tube life is quite satisfactory, and above which the life is short. As the commutation factor is increased to higher values beyond the threshold value, the tube life is not materially reduced.

The knee for the *GL-5545* curve has not

been determined as yet as all life tests in which the commutation factor has varied from 1.5 to 40 have resulted in no excessive gas clean-up or short life.

The two *GL-5545*'s described in test 1 in the paper, failed at approximately 4,000 hours because of low cathode emission. One of the tubes was damaged because of a poor socket contact connection to a filament pin on the tube. Since pressure gauges were not built into these tubes, the actual pressure change that occurred during life is not known. There was not, however, sufficient clean-up to cause the tube characteristics to shift outside of the published specifications.

Despite Edwards' comments in the last paragraph of his discussion, it is felt that in order to assure the highest degree of freedom from gas clean-up it is important to load the tube initially with as high a gas pressure as possible. It is not stated in the paper that the high gas pressure of the *GL-5545* is solely responsible for the increased resistance to gas clean-up that the tube exhibits over tubes of conventional design. As stated in the conclusion excessive gas clean-up is eliminated because of a combination of three factors of which the loading pressure is one.

If a tube of conventional design is loaded with a generous gas supply, and is subject to clean-up at low commutation factors, it is the writer's belief that the reason may lie in the fact that the second and third factors listed in the conclusion of the paper are lacking. In other words, the reason may be attributed to the fact that the anode is exposed and the volume of ionization is large.

A New Line of Oil-Filled Apparatus Bushings

B. N. BOWERS
ASSOCIATE AIEE

D. L. JOHNSTON
ASSOCIATE AIEE

FOR 32 years, oil-filled bushings have been applied to transformers and oil circuit breakers. Electrically and mechanically, the performance of these bushings has been remarkably good. However, because of recent trends in the design of electrical apparatus, some features of the bushings have become outmoded and it has been necessary to redesign the oil-filled bushing to obtain improvements which will meet the requirements of modern apparatus.

Chief among these requirements are hermetic sealing, low-cost maintenance, mechanical strength, and reduction in size and weight. To meet these needs, and in addition to provide for ease of installation and storage, the new bushing was designed.

This bushing has been in production for approximately two years for transformer and oil circuit breaker applications and is described in the following paragraphs.

Advantages of the New Construction

The hermetic sealed construction, accomplished by center-clamping and by the use of synthetic rubber gaskets prevents deterioration of the internal insulation of the bushing by moisture or by oxidation, thus eliminating the principal weakness of the older bushings. The rubber gaskets are oilproof and waterproof and retain their resiliency under all expected conditions of service.

The sealed construction has been thoroughly service-tested in liquid-filled, solid core bushings in the moderate voltage range. Approximately 100,000 bushings of this type have been placed in service in apparatus in the last 16 years and their very satisfactory service record is due largely to the use of synthetic rubber gaskets which provide a reliable gas and oil-tight seal.

The hermetic sealing of the new bushing also eliminates the need for costly maintenance which in the case of the old bushing meant renewing the oil or complete disassembly to clean and dry the internal insulation. Since the new bushing is completely sealed, the initial high dielectric strength is maintained through the years of service without costly operations on the internal insulation. In general, inspection of the oil level gauge and cleaning of the porcelain shell periodically are desirable.

High mechanical strength is obtained by center-clamping and the new bushings have demonstrated their ability to withstand without damage severe mechanical loadings under static conditions and also under interrupting tests applied to oil circuit breakers.

Reduction in diameter has been obtained by the center-clamped construction which requires no cemented-on clamping rings and by adjustment of internal insulation stresses to obtain maximum efficiency of the dielectric structure. The reduced diameter permits the use of smaller bushing-type current transformers, the elimination of mounting adapters, and less complicated apparatus covers, all of which are favorable to simplification of design and smaller apparatus.

Reduced weight which accompanies reduced size, facilitates packing for shipment, handling, and installation.

Installation of the new bushings requires no special equipment. Lifting eyes in the support flange provide means for attaching lifting cables.

The construction used in these bushings has inherently low power factor and thereby is particularly suited to power factor testing when desired for checking the condition of the bushing.

Application

The new bushing is available, with or without the capacitance tap feature, for application to transformers in voltage ratings from 92 to 360 kv, and to oil circuit breakers in ratings from 115 to 196 kv.

Standard bushings for oil circuit breakers are equipped with the capacitance tap which is also available for transformer bushings.

Bushings in ratings from 115 to 230 kv have mounting flanges which are interchangeable with the superseded bushings. The elimination of the bottom clamping ring has permitted shortening the bushings approximately 3 inches. For replacements in oil circuit breakers, a suitable adapter can be provided to compensate for the shortened length, and thereby readily adapt the new bushings to the older circuit breakers.

Bushings in the foregoing ratings for transformers which have detachable cable terminal parts may be interchanged with the old bushings having the same ground sleeve length, since the terminal studs which are brazed to the transformer leads are interchangeable. Differences in height between the old and new bushings may in some instances require changing the length of the cable leads.

Typical and special applications of the new bushing are shown in Figures 1, 2, 3, and 4.

A comparison of the sizes of the old and the superseding new bushings for oil circuit breakers is given in Table I. Standard bushings in ratings 92 to 230 kv are shown in Table II.

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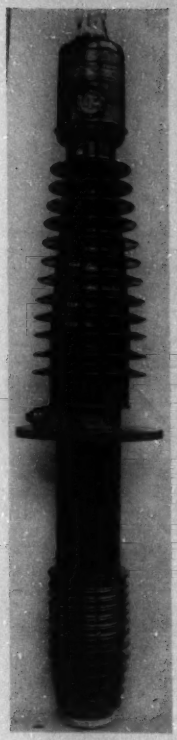
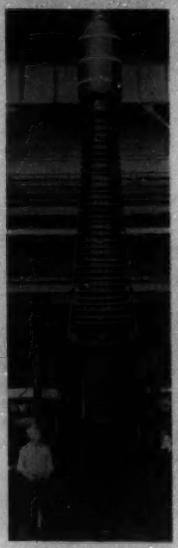
B. N. Bowers and D. L. Johnston are both with the high voltage bushing engineering division of the General Electric Company, Pittsfield, Mass.





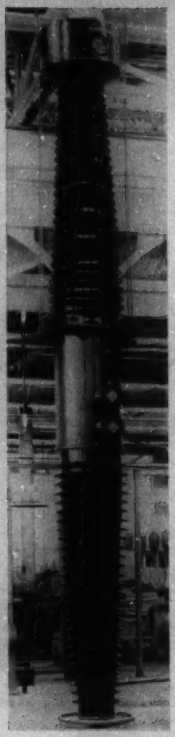


Figure 1 (left). 138 kv capacitance bushing for oil circuit breaker service

Figure 2. 360 kv capacitance bushing for transformer service

Figure 3 (right). 360 kv (1,300 kv, basic insulation level) capacitance bushing with built-in current transformers for outdoor switchgear

General Description

Principal features of the new bushing are:

1. Bushing is center-clamped, placing porcelain parts under compression, in which stress porcelain is strongest.

2. Gaskets at ends of porcelain shells are confined in recesses of proper depth to insure pressure-tight joints without metal-to-porcelain contact.

3. High-pressure multicoil spring assembly maintains a safe pressure on gasketed joints under all conditions of operation.

4. All gaskets are of synthetic rubber which is oilproof and waterproof, and retains its resiliency under all expected conditions of service.

5. Internal insulation is of oil-permeable barriers and oil ducts in combination, insuring high dielectric strength under testing and operating voltages.

6. Excepting the dome, all castings in ratings 115 to 161 kv have been eliminated. High strength brass forgings have replaced the castings in most nonmagnetic parts.

Figure 5 shows a cross-sectional outline of a 138-kv capacitance bushing which is described herein.

The center tube of the bushing is of high-strength copper, threaded and sweated into the forged brass bottom washer which has a side drain hole sealed by a threaded plug and copper-asbestos gasket.

An electrode, consisting of an aluminum tube insulated with adhesive-bonded oil-permeable paper is assembled down over the center tube and held in position by a brass collar locked to the center tube by set-screws. A spiral contact spring of phosphor-bronze insures a permanent connection between the center tube and electrode.

The support is of fabricated steel, electric-welded. In general the mounting flange of circuit breaker bushings is overhung to provide a rocker-type mounting joint and for transformer application the flange is flat with a gasket recess 1/8 inch deep. Interchangeability between old and new bushings is not impaired by these differences in the transformers and circuit breaker bushing flanges. However, it is not practical to adapt old bushings to oil circuit breakers originally equipped with new bushings.

Transformer bushings are equipped with detachable cable (draw lead) accessories. The bottom washer is of smooth contour with no sharp projections.

Oil circuit breaker bushings use the center tube for the conductor. The bottom washer has a spherical contact surface which permits ready adjustment of the circuit breaker interrupter which is flanged-clamped to the bottom washer of the bushing.

The top and bottom porcelain shells are of high quality, 1-piece porcelain with no gasketed joints (except in the 2-piece top shells above 230 kv), uniformly glazed on the outside surfaces and with accurately ground gasket surfaces at the ends. Both shells are assembled against flanges at the ends of the support and are held in position by center-clamping of the structural parts of the bushing. Gaskets at the ends of the shells are confined in recesses in the metal flanges. Bushings in ratings up to 161 kv have gaskets 3/16 inch thick, confined in recesses 5/64 inch deep. Gaskets for bushings of higher voltage ratings are 1/4 inch thick.

The insulating cylinders are of adhesive-bonded oil-permeable paper held in position by T-headed spacing blocks of kiln-dried maple. The spacing blocks are located at the top of each cylinder and are locked into grooves cut into the surface of the next smaller cylinder or electrode insulation. The ducts alternating with the cylinders are filled with oil. Each of the insulation cylinders has a nonmetallic conducting rescon equalizer embedded in the cylinder, and proportioned to provide safe internal voltage gradients and, in combination with the terminal and ground shields, suitable voltage distribution in the external dielectric fields of the porcelain shells. The terminal shield is located inside of the top end of the top shell. Actually, no ground shield is built into the bushing. The ground shield function is, however, performed by a concentric nonmetallic conducting rescon plate embedded in the largest diameter insulating cylinder and grounded through a tap which is connected to the support. This conducting plate extends a few inches above and below the ground sleeve of the support in an assembled bushing.

Figure 4. 92-kv bushing with expansion chamber integral with support, for wall entrance service

Figure 6 shows the details of the top end of the 138-kv bushing. At the top end of the bushing, an expansion chamber or dome of heat-treated high-strength aluminum provides for change in oil volume due to temperature variation. The expansion space of 20 per cent of the oil volume in the bushing limits the maximum pressure in operation to a moderate value. A magnetic gauge in one side of the dome indicates the level of the oil in the bushing and is easily read from the ground under most conditions of operation. The dome is amply protected from weather by a special baked-on finish. The filling hole in the dome is sealed by a threaded metal plug and a confined synthetic rubber gasket. A larger threaded hole at the outside of the dome provides for attaching a fixture which permits assembling the sealing plug and gasket with nitrogen pressure in the bushing. After sealing, a protective plug is assembled in the outer threaded hole.

The spring assembly at the top end of the bushing compensates for thermal expansion of the structural parts, thereby maintaining safe clamping force under all conditions of operation. The spring assembly consists of many small steel coil springs arranged concentrically around the center tube and enclosed in a toroidal diaphragm of spun copper. Forged brass washers at the ends of the springs prevent distortion of the diaphragm.

The forged brass cover which is threaded to the center tube is tightened down against the spring assembly to apply the required center-clamping force to the bushing structure and to all major gasketed joints. Synthetic rubber gaskets on top and bottom sides of the diaphragm are confined in recesses in the adjacent parts. Metal-to-metal contact is made from dome-to-diaphragm-to-cover at the inner edge of each gasket recess to insure permanent electrical contact. A small threaded synthetic rubber gasket, compressed by a phosphor-bronze spring forms a seal which prevents leakage of oil between the threads of the cover and center tube.

Bushings which are equipped with detachable cable accessories have a forged brass cap which screws into the cover, a forged copper terminal stud held into the cap by a pin, and a forged brass terminal cap which screws down over the terminal stud against the cap. Each joint below the cap and terminal cap is sealed by a synthetic rubber gasket confined in a recess. Bushings which use the center tube as a conductor have a terminal cap which screws into the cover and is sealed to the cover by a synthetic rubber gasket as described before.

In bushings equipped with the capacitance tap feature, details of which are shown in Figure 7, the outer insulation cylinder which is known as the capacitance cylinder is of resin-bonded paper and has two concentric, nonmetallic conducting plates of rescon which form the tap and the ground plates respectively. These plates extend from a few inches above the support sleeve down to a few inches below it. A copper electrode 0.005 inch thick and 2 inches wide is wound into the cylinder in contact with each rescon plate to provide means for making contact for tap and ground connections. A spring contact is made with the inner tap plate, through a large hole in the outer ground plate which has a soldered contact and is grounded to the support by a copper braid. The tap plate has a nominal open circuit potential of 3,600 volts to ground.

A capacitance tap chamber just above the mounting flange of the support encloses a small bushing of molded insulation with a spring contact at the inner end which makes contact with the inner plate of the capacitance cylinder. The outer end of this bushing is normally grounded by means of the cap but for operation in connection with a potential device, the cap is removed and replaced by a potential device cable connection, the chamber being filled with a viscous insulating compound through a fill hole in the top and sealed by a pipe plug for this condition of operation. A round section gasket of synthetic rubber seals the joint between the tap chamber and the cap or potential device connection.

Table I. Comparison of Old and Superseding New Bushings Rated 1,200 Amperes for Oil Circuit Breakers

Rating	Old, Inches	New, Inches
115 kv		
Live metal, top end, diameter	10.8	10.8
Live metal, bottom end, diameter	8.8	7.6
Height above flange	63.0	57.1
Length below flange	44.7	41.6
Oil content, gallons	17.5	8
Maximum diameter below flange	14.5	10.8
138 kv		
Live metal, top end, diameter	10.8	10.8
Live metal, bottom end, diameter	8.8	7.6
Height above flange	70.4	66.0
Length below flange	48.4	45.3
Oil content, gallons	26.5	14.5
Maximum diameter below flange	17.0	12.8
161 kv		
Live metal, top end, diameter	10.8	12.2
Live metal, bottom end, diameter	8.8	7.6
Height above flange	77.0	74.8
Length below flange	52.0	48.8
Oil content, gallons	42.3	23.3
Maximum diameter below flange	19.1	14.8
196 kv		
Live metal, top end, diameter	13.5	15
Live metal, bottom end, diameter	15.0	13.7
Height above flange	92.6	112.4
Length below flange	67.3	67.7
Oil content, gallons	91	55
Maximum diameter below flange	24.5	18.8

Assembly

In assembling the bushing, the cover is screwed down against the spring assembly.

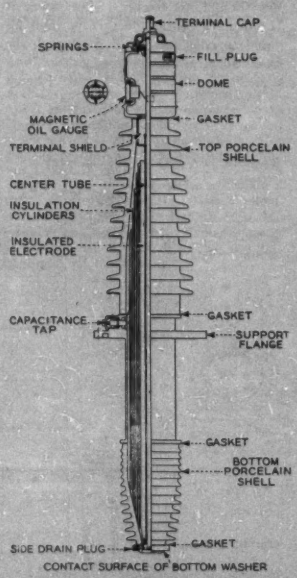


Figure 5. Cross-sectional outline of 138-kv capacitance bushing

Table II. Application Data for Standard Bushings in Ratings 92 to 196 Kv for Conservator and Sealed Transformers and Oil Circuit Breakers

Rating		Exposable Length Ground Sleeve, Inches		
Circuit Voltage, Kilovolts	Current, Amperes		Conductor	Application
92	400	13¾	cable‡	*
	400	26¾	cable‡	**
	1,200	13¾	tube	*
	1,200	26¾	tube	**
115	800	13¾	cable‡	*
	800	26¾	cable‡	**
	1,200	22½	tube	†
138	800	13¾	cable‡	*
	800	26¾	cable‡	**
	1,200	22½	tube	†
161	800	13¾	cable‡	*
	800	26¾	cable‡	**
	1,200	22½	tube	†
196	800	31½	cable‡	**
	1,200	31½	tube	†

* Conservator transformer
** Sealed transformer
† Oil circuit breaker
‡ Detachable

The torque necessary to give the correct value of center-clamping force is applied to the cover at assembly. The gap between the cover flange and the dome is also used as an approximate indication of the applied center-clamping force.

The assembled bushing is subjected to a vacuum treatment at a pressure of less than 1,000 microns of mercury at 110 degrees centigrade. It is then allowed to cool and is filled through the drain hole in the bottom washer with degasified insulating oil of controlled high quality. An oil pressure test of 20 pounds per square inch is applied for 15 minutes and inspection is then made for leakage. Although the vacuum treatment is sufficient to reveal any leakage, the pressure test is a final confirmation of pressure-tightness. After the oil level has been adjusted, the bushing is sealed pressure-tight through the filling hole in the dome with a cushion of dry nitrogen gas at ten pounds per square inch.

Test Data

Electrical tests on one 138-kv bushing gave the following results:

Power factor, 0.96 per cent
Capacitance, 252 $\mu\mu f$

60 Cycle
Dry flashover, 422 kv
Dry withstand (1 minute), 335 kv

Impulse
Positive critical flashover, 749 kv
Positive withstand, 650 kv
Negative critical flashover, 757 kv
Positive steep wave, flashover, 1,090 kv*
Negative steep wave, flashover, 1,075 kv*

In an effort to determine the ultimate dielectric strength a 138-kv bushing was subjected to the following 60-cycle tests.

1. Fifty flashovers, each followed immediately by a 1-minute test of 335 kv, were applied with 1-minute intervals. The first flashover was 386 kv—the fiftieth, 400 kv.

2. The bushing was subjected to the specified standard 1-minute value of 335 kv for one hour with no indication of distress.

3. In order to apply higher voltage stresses to the internal insulation, the bushing was immersed in the oil in the test tank to the third petticoat of the top shell above the support and withstood ten snap-overs to the edge of the test tank at voltages up to 400 kv and three 1-minute withstand tests at 375 kv.

* At rate of approximately 1,000 kv per microsecond.

Power factor values before and after the tests were 0.68 and 0.73 per cent, respectively, indicating no deterioration of insulation. Examination of the internal insulation of the bushing following the tests revealed no evidence of impairment.

Routine and design electrical tests on one 230-kv capacitance bushing for transformer service gave the following results:

Routine
Power factor—0.78 per cent, 60-cycle flashover—621 kv
Capacitance: 410 $\mu\mu f$, 60-cycle 1-minute withstand—545 kv
Tap voltage: 3,420 volts

Design
60 cycle, wet flashover—503 kv
wet 10 second withstand—445 kv
Impulse, positive critical flashover—1,130 kv
Positive withstand—1,050 kv
Negative critical flashover—1,340 kv
Negative withstand—1,050 kv

Conclusion

This paper has described a new line of high voltage bushings in voltage ratings from 92 kv to 360 kv which meet AIEE and the National Electrical Manufacturer's Association Standards and which are applicable to transformers and oil circuit breakers as well as to other types of service. These bushings were designed to meet the needs of modern apparatus and are constructed to give reliable service with a minimum of maintenance.

At the present time, standardization and consequent reduction in the number of special designs for a given voltage rating is under way. It is anticipated that the new bushings can be applied with a minimum of designs and thus contribute to this trend. In the broad interest of standardization and its far-reaching benefits the authors recommend that consideration be given to the elimination of special features wherever possible.

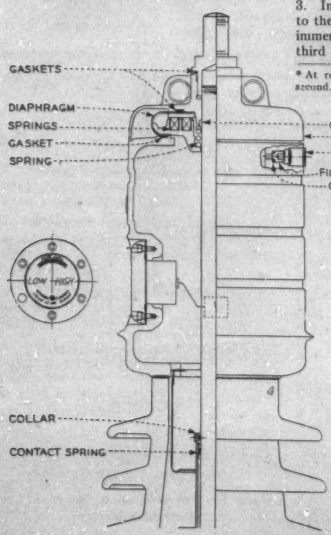
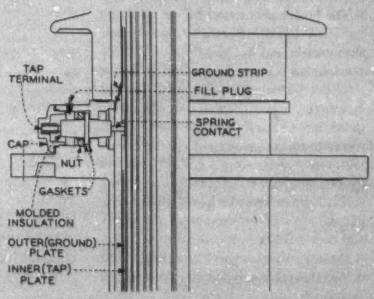


Figure 6 (left). Cross-sectional view of dome assembly

Figure 7. Cross-sectional view of capacitance tap feature



Discussion

H. L. Cole (Westinghouse Electric Corporation, Sharon, Pa.): Bowers and Johnston have described the design and development of a new and improved oil-filled bushing which incorporates features well recognized by most engineers as contributing to long life and low maintenance. A redesign of a standard line of high-voltage apparatus bushings, requiring new porcelain casings and a new type of construction, is a major development project. The authors are to be commended for presenting this new equipment to the industry.

The new General Electric bushing embodies many of the principles of the Westinghouse condenser type described in an Institute paper[1] five years ago. I would like to discuss briefly the design features emphasized in the present paper.

Hermetic sealing of oil-filled apparatus, with elimination of breathing, has been advocated by the Westinghouse Electric Corporation for years, not only for bushings but for high-voltage power transformers as well.

The use of the capacitor principle in bushing design makes it possible to obtain maximum efficiency of the dielectric structure as pointed out by the authors, and the internal volume of liquid is small enough to operate the bushing over wide ranges of temperature with a sealed cap of reasonable size and proportion.

End clamping of porcelains, with spring pressure on the porcelain gaskets, eliminates the need for more expensive and less effective cemented-on hardware. This construction has been proved by 15 years of centering clamping on our large condenser bushings. The steel springs in the cap not only provide for gasket pressure under changing temperatures but, when designed with ample pressures, will permit shipment of the bushing in a horizontal position with little or no additional support to the porcelains.

Oil-permeable paper is used as the major insulation in the new bushing. The writer questions the need of oil ducts in a construction having conducting layers for equalizing gradients in the dielectric and in which the over-all power factor is less than one per cent. In our opinion, the most efficient use of the paper is made when there is little or no adhesive bond, no oil ducts, ample vacuum treatment to remove air and moisture from the paper, and thorough impregnation with degassed oil. At various times, during our condenser bushing developments, we have considered the use of oil ducts through the condenser but have never adopted them.

Instead, there have been improvements in materials to give lower power factors and better control of processes to obtain uniform quality of insulation throughout the core of the bushing. Exhaustive life tests have been made, with assembled bushings entirely immersed in 90 degrees centigrade oil and operated for days at voltages in excess of the standard 60-cycle withstand values, to prove that oil impregnated paper condenser bushings without adhesive bond on the active insulation and without oil ducts through the insulation are electrically and thermally stable. Oil ducts, when used in condenser bushings, add relatively little to the electrical strength because most of the strength is obtained from the solid insulation. Omission of the oil ducts and a more generous use of the "conducting equalizer layers" would permit even smaller bushing diameters than those described in the paper. The bushing would have a higher capacitance, and potential devices with larger volt-ampere burdens would be permissible.

Magnetic oil gauges, used on condenser bushings and other liquid-filled electrical apparatus for many years, have proved their superiority to the old fashioned glass oil gauge. A magnetic gauge with alloy magnets of high permanence, properly shielded to prevent demagnetization when the bushing is subjected to heavy short circuit currents, eliminates the possibility of oil deterioration by sunlight and permits better visibility of the oil level.

The authors state that porcelain up to and including 230 kv is one piece. Does that mean that the porcelain is made in sections which are then united by means of a fired glaze?

In closing this discussion, the writer wishes to compliment the authors in presenting a clear and concise description of a new and modern bushing.

Reference

1. Oil-Impregnated Paper High-Voltage Condenser Bushings for Circuit Breakers and Transformers, H. J. Lingal, H. L. Cole, T. R. Watts. AIEE Transactions, volume 62, 1943, June section, pages 289–74.

B. N. Bowers: We are glad to have H. L. Cole's able discussion of the paper, which in many ways emphasizes the important features incorporated in the new bushings. Since one of the features discussed, namely, "hermetic sealing" involves the gasket material, it seems desirable to comment briefly on this subject.

Composition cork gaskets had been used by the industry up until 1932 for sealing joints of oil-filled apparatus and various adhesives were used to cement the gaskets to metal and porcelain surfaces. For seals against oil pressure composition cork was generally satisfactory, but where oil was not in contact with and applying positive pressure to the joint, and particularly where a partial vacuum existed, considerable difficulty with leakage was experienced.

Recognizing the need for a gasket which would withstand pressure whether in contact with oil or not, General Electric pioneered in the development of gaskets of oil-resistant and moisture-resistant rubber and started to use them in moderate voltage bushings in 1932. Modern gaskets of synthetic rubber are manufactured to rigid specifications, the outgrowth of many years of experience which has made hermetic sealing possible. Cole inquires regarding the 1-piece porcelain shells which are used in ratings up to and including 230 kv. These shells are cast in sections, and turned all over to proper dimensions. The mating parts for the joints are accurately fitted to gauges. Each section is then placed one upon the other with the joints coated with a special fusing glaze. The shells are placed in the firing kiln and fired under accurately controlled temperature and atmospheric conditions. During this firing cycle the body of the porcelain is properly vitrified and the joints are fused or welded together by the glaze into one solid piece.

Each shell of this type is subjected to a transverse loading test, which assures ample mechanical strength.

Periodic tests to destruction are made as a means of controlling the factor of safety.

In a center-clamped structure such as is used in the new bushing the shells are placed in compressive stress only, which is a very favorable operating condition.

Electrical Properties of Ceramics as Influenced by Temperature

E. W. LINDSAY
ASSOCIATE AIEE

L. J. BERBERICH
FELLOW AIEE

Synopsis: Data on dielectric strength, resistivity and power factor are presented for a high-tension porcelain, a zircon porcelain and an alumina porcelain. Additional data on mica and fused silica taken from the literature are included for comparison purposes. The temperature range covered in the experimental work extends from 25 to 1000 degrees centigrade. When the logarithms of the dielectric strength, resistivity or power factor are plotted against the reciprocal of the absolute temperature, two straight lines of different slopes result in each case. The intersection of these two lines define a transition temperature between the disruptive and thermal regions of electric breakdown. An interesting feature of this investigation is the discovery that the transition temperatures as determined by the dielectric strength coincide approximately with those determined by resistivity or power factor. The transition temperature gives some indication of the maximum temperature beyond which the material should not be used in practice. Considerable advantage of zircon and alumina porcelains over high tension porcelain for high temperature applications are indicated. The data are discussed in the light of existing conduction and electric breakdown theories.

HIGH TENSION PORCELAIN, consisting largely of feldspar, clay and flint, has long been and still is one of the more important ceramics used in the electrical industry. It owes its unique position to the low cost of raw materials, ease of fabrication into wide variety of shapes and sizes, and uniform quality of the final product for application at ordinary temperatures and low frequencies. In recent years, and particularly during World War II, an increasing number of applications have arisen in which high dielectric strength at elevated temperatures is a prime requirement. In such applications, high voltage porcelain is not very satisfactory, for its dielectric strength begins to fall rather sharply with temperature above 100 degrees centigrade.

For the higher temperature applications, several special porcelains, among which are the zircon and alumina porcelains, have been developed. Both of these porcelains made their first appearance in spark plug applications, where resistance to thermal shock is perhaps just as important as high dielectric strength at elevated temperatures. During World War II, alumina dominated the important aircraft spark plug field and zircon found a number of specialized applications. Because of their superior electrical, mechanical, thermal and chemical properties, these porcelains are finding an increasing field of usefulness in both low and high frequency applications. Despite the importance of these porcelains, there appears to be very little data available on their dielectric behavior at elevated temperature. An investigation of the dielectric properties was, therefore, made on high tension, zircon and alumina porcelains over a wide range of temperature extending, in some cases, from 25 to 1,000 degrees centigrade. During the course of this work, a striking correlation between the dielectric strength, as influenced by temperature, and resistivity and power factor as influenced by temperature, was observed. A similar correlation was also found for mica and fused silica. The breakdown data and the theoretical significance of these correlations are discussed in some detail.

Samples and Testing Procedure

The samples consisted of simple, unglazed disks having diameters of 4 and 8 inches and thicknesses of 0.125 and 0.25 inch. The compositions as determined by chemical analysis of the completely vitrified bodies are given in Table I. The main crystalline constituents listed at the bottom of the table were determined by means of an X-ray spectrometer. It is well known and was substantiated by X-ray investigation that high tension porcelain consists of two crystalline phases, namely, quartz (SiO_2) and Mullite ($3Al_2O_3-2SiO_2$) with the former usually predominating. These two crystalline phases are bonded together by an alkaline aluminum silicate glass matrix. Zircon porcelain consists largely of oxides of zirconium, silicon, and calcium. It is produced by firing a mixture of zircon ($ZrO_2 \cdot SiO_2$), clay and alkaline earth oxide fluxes such as CaO. The fired body is largely crystalline zircon bonded by an alkaline earth aluminum silicate glass. Table I shows that the alumina body consists of a high percentage of alumina (Al_2O_3) with some silica (SiO_2) and smaller amounts of other oxides. This body is compounded from alumina and a small amount of clay or other fluxing material. X-ray investigation shows that the fired body is composed of α-alumina crystals bonded together by a small amount of alkaline earth glass.

Though all of the bodies may be considered as a glassy matrix in which the crystalline constituents are imbedded, it is difficult to determine the relative amounts of crystalline and amorphous or glassy phases. After examination of the X-ray patterns, however, it was estimated that high-tension porcelain has a rela-

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E. W. LINDSAY is research engineer in the insulation department and L. J. BERBERICH is manager of the physical section in the insulation department, both of the Westinghouse Research Laboratories, East Pittsburgh, Pa.

The writers are indebted to C. F. Hill, R. Russell, Jr., C. N. Works and M. H. Welsh for their suggestions and assistance in carrying out the work described in this paper. The writers also wish to express their appreciation to G. I. Gilchrest of the Derry works of Westinghouse Electric Corporation for supplying samples of high tension and zircon porcelain bodies, and to H. W. Ryland of the Coors Porcelain Co., Golden, Colo., for supplying the experimental alumina porcelain bodies.

Table I. Composition of Various Porcelain Bodies

Constituent	Composition in Per Cent		
	High Tension Porcelain	Zircon Porcelain	Alumina* Porcelain
SiO_2	68.72	36.96	6.69
Al_2O_3	23.90	6.08	90.98
TiO_2	—	0.24	0.06
ZrO_2	—	49.72	—
Fe_2O_3	1.80	0.23	0.20
Na_2O+K_2O	3.46	0.13	0.21
CaO	0.25	6.48	1.08
MgO	0.12	0.14	0.66
Total	100.25	100.00	99.88
Total flux	5.83	6.75	1.95
Main crystalline constituent	Mullite ($3Al_2O_3 \cdot 2SiO_2$) Quartz (SiO_2)	Zircon ($ZrO_2 \cdot SiO_2$)	Alumina (α-Al_2O_3)

* This high alumina body is not commercially available.

tively high percentage of glass and that the alumina porcelain, on the other hand, has a low percentage of glass, while the zircon porcelain is intermediate. The glassy phase, particularly if it contains alkali metal oxides, has an important bearing on the electrical behavior of the body at higher temperatures.

Both a-c and d-c dielectric strength tests were made using the American Society for Testing Materials (ASTM) *D149-44* one-minute step-by-step testing procedure. This procedure produces results which have greater practical significance than is the case where the voltage is raised rapidly until failure occurs and it also allows time for the consummation of a thermal breakdown. It does have the disadvantage that the exact temperature of the sample at the time of failure is not known. At the higher temperatures, and, in particular, when the resistivity of the samples falls to less than 10^{10} ohm-centimeters, the sample temperature rises appreciably above the ambient temperature. Since this temperature rise is variable and usually unknown, only the ambient temperature at the time of breakdown was recorded. This does not affect the accuracy of the data appreciably except at the highest temperatures of test for each individual material.

Tests at temperatures about 125 degrees centigrade and below were made under oil using 1-inch diameter electrodes whose edges were rounded to 1/8-inch radius. A few tests were made in this same temperature range in air and the values were somewhat lower than those made under oil because the edge discharges were more severe in air. All of the tests above 125 degrees centigrade were made in the furnace shown in Figure 1. The electrodes are 3/4 inch in diameter and constructed of stainless steel because of the high temperatures involved. As the diagram shows, the inside of the furnace was lined with a grounded steel box of sufficient size that the field at the electrode would not be seriously affected. The furnace is equipped with automatic temperature control for temperatures up to 1,000 degrees centigrade.

Only a few breakdown tests were obtained in the temperature range of 150 degrees to 300 degrees centigrade on zircon and alumina bodies, because the breakdown strengths in this temperature region are still high enough to cause surface flashovers, especially in the case of the thicker and smaller diameter samples. Also, in this temperature range, the electrical discharges around the electrodes became so severe that punctures took place as much as one inch away from the edges of the electrodes. When the tests were made under oil, most of the punctures occurred at the edges of the electrodes, indicating that stress concentrations at the edges of the electrodes played a role. The results should, therefore, be interpreted as being representative of practical or apparent values rather than intrinsic values. Most of the punctures at the higher temperatures in the thermal breakdown region took place under the electrodes as might be expected.

The resistivity values were determined at low stresses (100–500 volts) by means of a general radio megohm bridge after one minute of electrification unless indicated otherwise. The electrodes consisted of fired-on silver films backed by 1/2-inch thick silver cylinders. The same electrode system was also used to measure the power factor at 100 kc. The equipment used to measure the power factor was a General Radio *716AS-1* bridge.

Experimental Results

The dielectric strength data as well as the resistivity data for the three ceramic bodies investigated are recorded in Figures 2, 3 and 4. All of the plotted points are the average of from three to ten individual breakdown tests. The scattering of the dielectric strength data was appreciable at the lower temperatures but this was negligibly small at the higher temperatures. The vertical scale on these graphs is logarithmic, and the horizontal is a reciprocal absolute temperature scale. These scales were chosen in plotting the dielectric strength and resistivity data since straight lines should be obtained, at least over limited temperature ranges. Referring first to the dielectric strength, it is observed that in all cases the data define, at least approximately, two straight lines. In the low temperature region, the dielectric strength is practically independent of temperature while above a certain temperature, which differs for each material and which will be referred to as the transition temperature, the dielectric strength falls rapidly with increased temperature. The region below the transition temperature is known as the disruptive or electric breakdown region while that above the transition temperature is known as the thermal breakdown region. Von Hippel and Maurer[1] report similar results for glass and show that such a behavior is to be expected of amorphous materials. In crystals the situation is somewhat different in that the d-c breakdown strength increases with temperature in the low temperature region until the transition temperature is reached when it decreases again, as is the case for amorphous materials.

Referring now to the resistivity data given in Figures 2, 3 and 4, it is observed that not only do these data also define two straight lines but that the transition temperatures for resistivity and dielectric strength approximately coincide. This coincidence of transition temperatures for resistivity and dielectric strength has not been observed previously, insofar as the writers know. This presents a new point of view which may help to throw further light on the nature of electric breakdown in solids.

This behavior holds not only for the ceramics investigated, but it was also found to hold for mica which is entirely crystalline and fused silica which is entirely amorphous. Data for muscovite mica reported by Hackett and Thomas[2] were replotted as shown in Figure 5. The smooth solid dielectric strength curves of Figure 5 represent the data as originally plotted on linear plot. The dashed lines were added to show that both the apparent or practical and the intrinsic or true dielectric strengths can be represented approximately by two straight lines, the intersection of which defines a temperature which is essentially the same

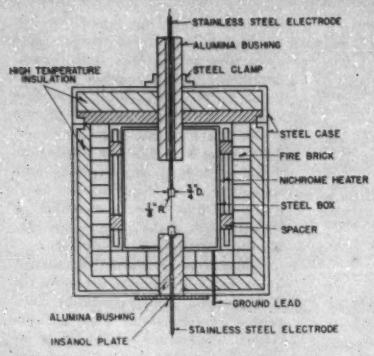

Figure 1 (left). High temperature oven and electrodes

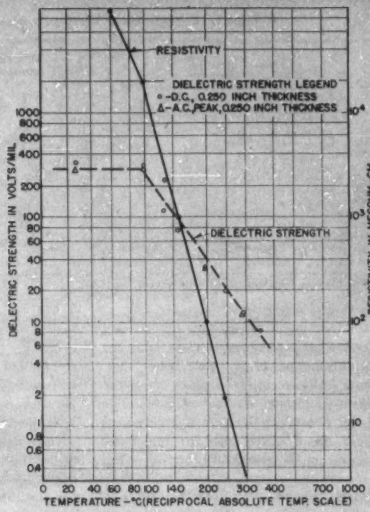

Figure 2. Variation of dielectric strength and resistivity of high tension porcelain with temperature

as that defined by the two lines for the resistivity data. This is also true for fused silica, data for which are given in Figure 6. The dielectric strength data were taken from Moon and Norcross.[2] The transition is, of course, never sharp for either dielectric strength or resistivity, but quite gradual, as would be expected from the simultaneous operation of both the high and the low temperature mechanisms. Moon and Norcross[2] have proposed three separate regions of breakdown for glass, but it appears that their intermediate region is merely the transition region between the disruptive and the thermal regions. The available data for fused quartz given in Figure 6 appear to be insufficient to define the slopes of the two lines very well. Since fused quartz is an amorphous material, its dielectric strength at low temperatures would be expected to be less influenced by temperature than Figure 6 indicates.

Figure 7 shows that the power factor as measured at 100 kc also can be represented approximately by two straight lines when the logarithm of the power factor is plotted against the reciprocal of the absolute temperature. Some of the power factor data were taken from a previous paper,[4] which contains power factor and resistivity data for a number of ceramic materials. The dashed vertical lines indicate the transition temperatures as determined by the resistivity data. The transition temperatures as determined by the power factor data do not agree exactly with those determined by the resistivity data, but this is to be expected since the power factor and resistivity measurements were made on different sets of samples whose composition may vary slightly.

Attention is called to two anomalies in the power factor behavior. The first involves the smaller slope of the power factor curve for zircon porcelain in the high temperature region than in the low temperature region. The reverse is seen to be true for all of the other bodies whose power factors are given in Figure 7. This abnormal behavior has been observed in three different zircon porcelain compositions and so far there has been no clue as to what characteristics of zircon are responsible for the anomaly.

The second anomaly concerns the very sharp power factor peaks observed in alumina after subjection to d-c voltages at high temperatures. Figure 8 shows the results obtained on a 1/8-inch thick sample of alumina after subjecting it to about 500 degrees centigrade and a d-c voltage of approximately 500 volts for 30 minutes. It will be observed that the peaks are quite sharp and become sharper with increasing frequency rather than shifting to a higher temperature as in the case of the usual type of polarization. There was no significant variation in dielectric constant in the temperature range from 20 to 100 degrees centigrade. It appears that the temperature, the magnitude of the voltage and the time of its application are all important factors in producing this phenomenon. A slight change in these conditions may change the magnitude of the peaks and the temperature at which they occur.

Under more severe conditions of electrolysis, the peaks do not appear at all. For example, when a sample was subjected to 1,000 degrees centigrade and 3,000 volts direct current for nine hours, no peaks were observed in the temperature range from 25 to 70 degrees centigrade.

This treatment, however, resulted in a more than ten-fold increase in power factor and an increase from about 9 to 13 in dielectric constant over the same temperature range. Moreover, the sample showed a strongly negative temperature coefficient of dielectric constant up to 70 degrees centigrade, whereas the untreated sample showed substantially no change in dielectric constant in the same temperature range. Much more work is required to ascertain the conditions and the mechanism by which such a peculiar type of polarization is produced.

Discussion of Results

For convenience of discussion, the transition temperatures of the various materials considered in this paper are summarized in Table II. This tabulation shows the superiority of the special porcelains, fused quartz and mica over high tension porcelain for applications involving high temperatures. The transition temperatures, besides being of theoretical interest, should also be of practical value since they represent the temperature above which it is inadvisable to apply a sizeable fraction of the breakdown voltage if cumulative heating and subsequent instability and finally breakdown is to be avoided.

Table II. Transition Temperature for Various Materials

Material	Transition Temperature, Degrees Centigrade
High tension porcelain	100
Zircon porcelain	220
Alumina porcelain	250
Fused silica	280
Muscovite mica	380

Perhaps the principal reason for the low transition temperature observed in high-tension porcelain is the relatively high alkali metal content as Table I shows. Warburg, as early as 1884, established that conduction in glass is electrolytic in nature[3] due largely to alkali metal ions, and that Faraday's laws of electrolysis were satisfied. Thus, potassium, and particularly sodium because it has a relatively small molecular volume, are probably responsible for most of the conductivity in all of the bodies. Zircon porcelain has a considerably higher transition temperature than high tension porcelain which can certainly be attributed partly to its low alkali metal content. Table I shows that the flux for this body is largely calcium oxide which does not contribute greatly to conductivity in glass. The still higher transition temperature observed for the alumina body can be attributed partly to its low alkali metal content, and partly to its relatively higher melting point as is evidenced by the fact that a considerably higher firing temperature is required for this body than for the other two bodies. Thus, it may be concluded that large additions of alkali metals are undesirable in ceramic bodies designed for use at high temperatures.

Referring again to Figures 2, 3 and 4, the following further deductions can be made from the plotted data:

1. The resistivity of high-tension porcelain is considerably lower over the whole temperature range than that for the other two bodies.
2. The dielectric strengths in the low temperature or disruptive region are not significantly different for the three bodies and differences appear mainly in the high temperature or thermal region.
3. The d-c dielectric strength for all three bodies appears to be slightly higher than the 60-cycle a-c peak dielectric strength.
4. The dielectric strength of the $^1/_8$-inch thick samples is somewhat higher than for the $^1/_4$-inch thick samples.

All of the bodies show a large decrease in resistivity with increase in temperature. It has been shown by Kraus and Darby[4] that ionization of the constituents of glass changes only slightly with temperature. If this be true for the ceramic bodies, the increased conductivity at the higher temperatures must be due to increased velocity of the ions. The velocity of the ions can increase only due to a corresponding decrease in the viscosity of the medium in which they are moving. Bryson[5] and also Littleton and Morey[7] have reported data on glass which show that the resistivity decreases with temperature in much the same way that viscosity of the glass does. This explains why the higher melting bodies, which should have higher viscosities, are expected to show a lower conductivity at a given temperature, other things such as the degree of ionic dissociation being the same.

The coincidence of transition temperatures between two regions of breakdown and two regions of conductivity naturally leads one to suspect that both transitions might be ascribed to the same cause. The possibility of a change in structure occurring in the various ceramics at the transition temperature was carefully investigated. X-ray spectrographic studies were made which revealed the crystalline constituents in the various bodies and these are listed in Table I. The literature revealed that the temperatures at which changes in structures occur in all of the crystalline components are much higher than the transition temperatures found in this work. The possibility of a second order transition or the like taking place in the glassy or amorphous phase was also investigated. Coefficient of expansion data for the various bodies were carefully obtained over a wide range of temperature. In no case was a change in slope of the expansion curve observed in the region around the transition temperatures. Thus, there appears to be little likelihood that the transition temperatures observed are due to a change in

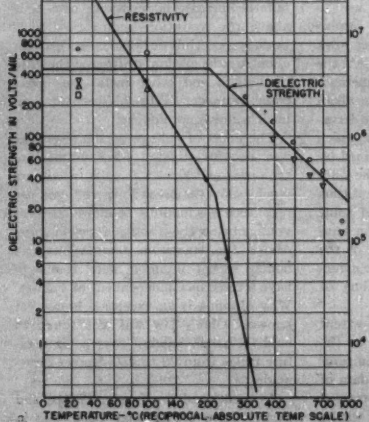

Figure 3 (left). Variation of dielectric strength and resistivity of zircon with temperature

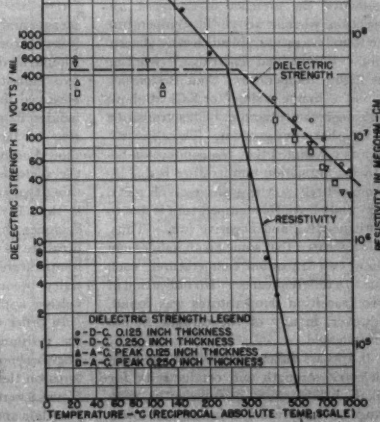

Figure 4. Variation of dielectric strength and resistivity of alumina porcelain with temperature

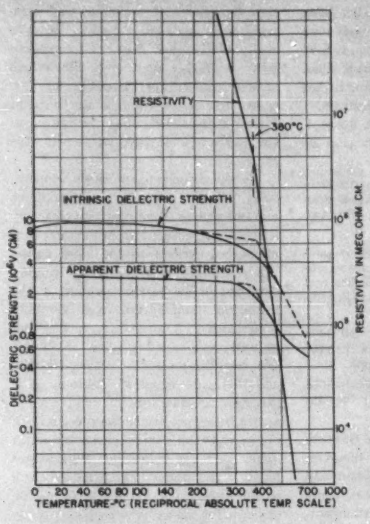

Figure 5. Variation of dielectric strength and resistivity of muscovite mica

(Data taken from Hackett and Thomas)

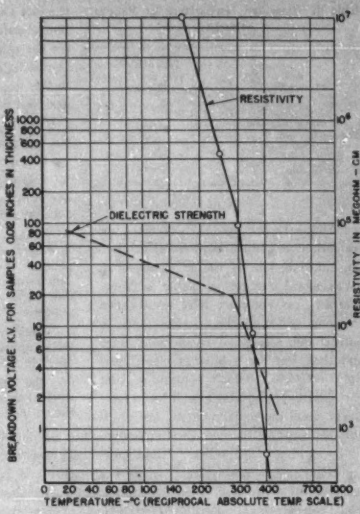

Figure 6. Variation of dielectric strength and resistivity of fused silica with temperature

(Dielectric strength data taken from Moon and Norcross)

structure of the ceramic. The existence of two or more distinct straight lines with different slopes for alkali halides when the logarithm of the conductivity is plotted against the reciprocal of the absolute temperature has been reported by a number of investigators.[8–12] Brennecke,[8] for example, has reported a change of slope in the resistivity data for KCl at about 230 degrees centigrade. He found that, below this temperature, the resistivity is a function of the time of voltage application and this indicated the presence of dielectric absorption, whereas above this temperature he found no measurable dielectric absorption. Phipps and Partridge,[10] working at higher temperatures than Brennecke, found a change in slope for KCl at about 445 degrees centigrade. This transition is not sharp but gradual. These investigators concluded that the conduction below the transition temperature is due almost entirely to the positive alkali metal ions and above this temperature is due to both positive and negative ions. Thus, it is indicated that some alkali halides can show two transition temperatures, each due to a different cause. No indication of two transition temperatures was found, however, in any of the ceramic bodies investigated.

It appears difficult to find data on both dielectric strength and conductivity for alkali halides over a sufficiently wide range of temperature to include the lower transition temperatures. Von Hippel and Maurer[1] give breakdown data on KBr over a range of temperature extending from −160 degrees centigrade to 190 degrees centigrade. When these data are plotted on a logarithm versus $1/T$ plot, a transition temperature of about 75 degrees centigrade is obtained. Brennecke[8] gives resistivity data on several samples of KBr, one of which appears to have a transition temperature at about 70 degrees centigrade which is reasonably close to that determined by dielectric strength. The data, however, are so meager and the agreement among various investigators is so poor that further work should be done to confirm this. Of the two transition temperatures sometimes observed in conductivity data on alkali halides, it appears that it is the one occurring at the lower temperatures which should correspond to one determined by the dielectric strength data. The only thing that can be said at the present time is that Brennecke[8] has presented some evidence that this lower transition temperature is in the neighborhood of the temperature above which the conductivity becomes independent of time of voltage application.

In order to determine to what extent the resistance varies with time in the neighborhood of the transition temperatures on the ceramic bodies, the data of Figure 9 were obtained. For high-tension porcelain and alumina porcelain, it will be observed that below the transition temperatures (Table II), the resistance increases with time, while above the transition temperature the resistance is constant with time. Zircon porcelain behaves differently. Figure 9 shows the resistance increasing at even 76 degrees centigrade above the transition temperature. This peculiar behavior of zircon porcelain in contrast to that of other ceramics was also observed by Frey and Jesatko.[13] The exact reason for this difference in behavior was not established but the increase in resistance with time above the transition temperature could be due to electrolytic polarization brought about by the gradual drifting of the positive alkali and other metal ions away from the anode. Why this does not occur in all bodies is not known.

The effect of voltage on the resistance was also investigated. For voltages of the order of 100 to 500 volts, which are much below the breakdown voltages, the resistance was found to be independent of voltage below the transition temperature. Above the transition temperature, all bodies except alumina were found to have a resistance independent of voltage. Why alumina is an exception is not yet known. Ohm's law was also found to hold for alkali halides[8] over a temperature range from 20 to 450 degrees centigrade.

As mentioned previously, Phipps and Leslie[12] have found that both ions in

sodium chloride carry current above 500 degrees centigrade and below this temperature all of the current is carried by the positive sodium ion. This change from uni-ionic conduction to bi-ionic conduction occurs near the higher transition temperature, 450 degrees centigrade, that Phipps and Partridge[10] reported. Some evidence of bi-ionic conduction was also found for alumina porcelain at 1,000 degrees centigrade. The photomicrograph in Figure 10 shows cross-sectional views of the area directly under the electrodes of two 1/4-inch thick disks. The lower portion of the photomicrograph shows the disk after a d-c voltage of 3,000 volts had been applied for nine hours while the temperature was held at 1,000 degrees centigrade. Definite evidence of ion migration at both electrodes is apparent by the color change which extends partly through the disk from each face. The upper portion of Figure 10 shows the second disk after it was subjected to an a-c voltage of the same magnitude under otherwise similar conditions. No evidence of ion migration or color change is apparent with alternating current as is to be expected.

While bi-ionic conduction could explain the steeper slopes of the resistivity curves above the transition temperature, we have no evidence that it exists except at much higher temperatures. Since conduction due to the negative ion, just as is the case for the positive ion, decreases with decrease in temperature according to the law relating conductivity, σ with absolute temperature T, $\sigma = \sigma_0 e^{-1/T}$, only about one millionth of the conduction due to the negative ion is expected at the 250 degree centigrade transition temperature for the alumina porcelain compared with that which takes place at 1,000 degrees centigrade where evidence of bi-ionic was observed. Thus, the difficulty of establishing the temperature at which bi-ionic conduction is initiated can be appreciated.

The high power factor peaks for alumina porcelain shown in Figure 8 were considered in the light of a new polarization phenomenon in alkali halide crystals which was reported recently by Breckenridge.[14] This type of polarization is ascribed to the jumping of positive ions into vacant lattice sites. It appears possible that something similar could occur in alumina porcelain, but the evidence is against it because the experimental data are so different in the two cases. In the case of the alkali halides, the power factor and dielectric constant variations with both frequency and temperature are similar to those for the more usual types of dielectric polarization, whereas this is not true for the alumina porcelain body as has already been pointed out. All that can be said now regarding this peculiar behavior of alumina porcelain is that it must be related to a permanent displacement of charge which takes place at about 500 degrees centigrade upon application of d-c voltage. This phenomenon was not observed in any of the other bodies which were electrolyzed in a similar manner. It is possible, however, that the proper conditions for producing it in the other bodies were not found.

Passing now to dielectric breakdown, the actual breakdown is now generally believed to involve the release of an avalanche of electrons irrespective of whether it takes place in the disruptive or thermal region. There is plenty of evidence for this, since the final surge of current always takes place in so short a time that numerous attempts to obtain a complete record with high-speed oscillographs have failed. If ions alone were involved, the final process would be much slower. The disruptive and thermal regions differ only in the process by which the final stage is reached. In the disruptive region, the ions play an insignificant role, but in the thermal region the relatively large ionic conduction and the resulting heating effect promotes breakdown at a lower value than otherwise would be the case.

The recent theories on electric breakdown in solids all involve electronic processes. The most important ones are those proposed by von Hippel[17] and Frohlich.[18–21] The two theories are somewhat similar and differ only in the details. They both involve the formation of electron avalanches in the final stages which are produced by a restricted ionization-by-collision phenomenon somewhat analogous to that which occurs in gases. As the electrons gain velocity from the applied field, they are retarded by the

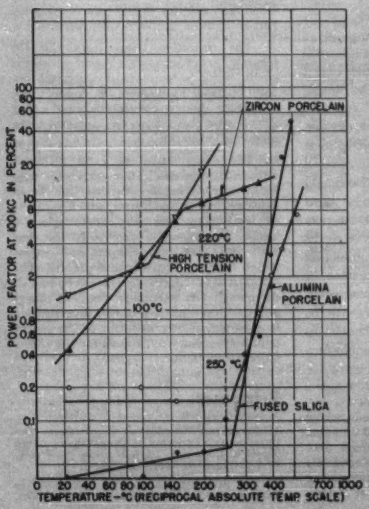

Figure 7. Variation of power factor of various porcelain bodies with temperature

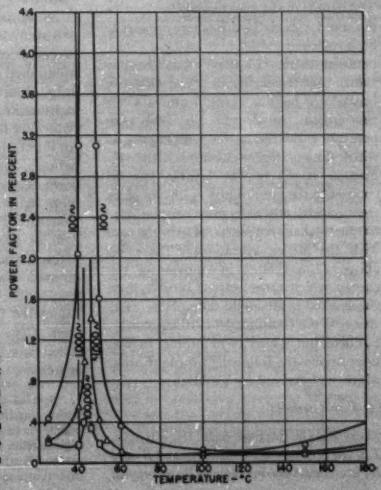

Figure 8. Variation of power factor of alumina porcelain with temperature at various frequencies after subjection to d-c voltage at high temperature

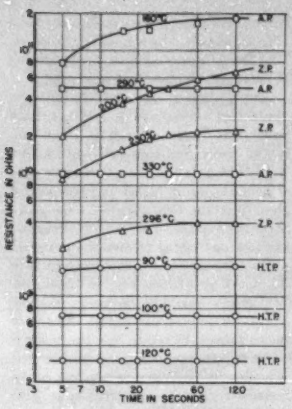

Figure 9. Effect of time and temperature on resistance of ceramic bodies

A.P.—alumina porcelain; Z.P.—zircon porcelain; H.T.P.—high tension porcelain

frictional effect of losing part of their energy to excite lattice vibrations. Breakdown takes place when the ionization becomes cumulative in spite of the frictional force. The positive space charge left by the displacement of electrons also plays an important role by distorting the field and weakening the material, thus resulting in cracks which have often been observed.[15–17]

Both theories have been found to be in qualitative agreement with experiment.[1,2] The details of the two theories will not be discussed, especially since there appears to be insufficient evidence to make a choice between them. Frohlich[21] has recently shown that the earlier theory of dielectric breakdown becomes invalid above a certain critical temperature T^1. This temperature is defined as the temperature at which the number of electrons in the conduction bands of the crystal has increased to the point where collisions of an electron with other electrons become more frequent than collisions of an electron with the lattice vibrations. Therefore, above T^1 a change in transfer of the energy which the electrons receive from the applied field should take place. The theory is too complicated to discuss in detail here, but it may be of interest to consider briefly the following simple relation which was derived by Frohlich:

$$\log F = K_1 + K_2/T$$

where F is the intrinsic dielectric strength, T is the absolute temperature, and K_1 and K_2 are constants involving the energy of electrons and the energy of the conduction bands. This relation states that the logarithm of the dielectric strength should decrease as the reciprocal of the absolute temperature. The relation applies only above the critical temperature T^1, and Frohlich cites the experimental data of von Hippel and Maurer[1] on soda lime glass and that of Thomas and Griffith[22] on varnishes as being in general agreement with the theory. The data for the ceramic bodies given in this paper are also in qualitative agreement with the theory. The transition temperatures repeatedly referred to in this work appear to be identical with Frohlich's critical temperature T^1 which he defines entirely in terms of an electronic mechanism. The fact that T^1 or the transition temperature has the same value for conduction as for breakdown should be of importance in the further development of breakdown theories. Some modification of the theory already appears to be necessary to take into account ionic conduction, which usually accounts for nearly all of the current flow in insulators up to, but not including, the final stage of breakdown. It certainly would be desirable to have data showing what proportion of total current is electronic at field strengths near the breakdown values. While much of the experimental data appears to be in qualitative agreement with the theory, further refinement appears necessary before quantitative agreement can be realized.

Summary and Conclusions

A study has been made of the electrical properties of a number of ceramics over a wide range of temperature. The superiority of several special porcelains over high-tension porcelain has been demonstrated for high-temperature applications. An interesting correlation was discovered between conductivity and electric breakdown behavior which should help throw some light on electric breakdown phenomena. The results are discussed in the light of existing theories of conduction and electric breakdown. Attention is directed to a number of unsolved problems with the hope that others will be stimulated into giving some thought to them. The more important conclusions which may be drawn from this work are as follows:

1. Special porcelains can be made whose major constituents are zircon and alumina, which have considerable advantage over high-tension porcelain, both electrically and mechanically, for applications involving high temperature.

Figure 10. Cross-section of alumina porcelain after subjection to 1,000 degrees centigrade and 3,000 volts

Top—a-c voltage; bottom—d-c voltage

2. When the logarithm of dielectric strength is plotted against the reciprocal of absolute temperature, the data define two straight lines of different slopes. The intersection of the two lines define a temperature which is called the transition temperature separating the disruptive and thermal regions of electric breakdown.

3. The transition temperature for high-tension porcelain is 100 degrees centigrade, that for zircon porcelain is 220 degrees centigrade, and that for alumina porcelain is 250 degrees centigrade. Mica and fused silica, in comparison, have transition temperatures of 380 degrees centigrade and 280 degrees centigrade, respectively.

4. It has been found that the transition temperatures for the dielectric strength data coincide with those for the resistivity data. This indicates that the underlying mechanisms in breakdown and conduction may be similar and this should help in throwing further light on electric breakdown in solids.

5. The transition temperatures for the ceramic bodies separate a region where the conductivity is dominated by dielectric absorption from that where the conductivity is largely due to electrolytic conduction. Zircon porcelain, however, appears to be an exception to this.

6. The experimental data on dielectric strength are in qualitative agreement with recent electric breakdown theories. Frohlich's critical temperatures, which separate the low and the high temperature regions of electric breakdown, appear to be identical with the transition temperatures determined by the resistivity and the dielectric strength data reported herein. Even though no quantitative agreement between theory and the practical data of this investigation is expected, some refinement of the theory appears necessary to account for the effects of ionic conduction.

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Discussion

E. J. Murphy (Bell Telephone Laboratories, Inc., Murray Hill, N. J.): There are at least two ways of interpreting the relationship between breakdown strength and resistivity which has been demonstrated by Lindsay and Berberich: (a) by an electronic process recently proposed by Fröhlich, and (b) by an electrolytic process. The theory of breakdown by electrons as proposed by Fröhlich yields in a natural way a breakdown strength which increases with increasing temperature. But experimental evidence has shown that above a certain temperature, depending on the material, the breakdown strength decreases with increasing temperature. Fröhlich has attempted to explain this by the intervention of electron-electron collisions, before breakdown occurs. This is certainly the more interesting way of explaining the relationship found by Lindsay and Berberich.

However, I have for several years held the following view of how conductivity effects breakdown in solids. For very low conductivities there should be no effect. But for higher conductivities, such as ceramics and other dielectrics attain at higher temperatures, there is an indirect effect due to electrolysis. The products of electrolysis create regions of higher resistance in some dielectrics near one or both of the electrodes. As a consequence the voltage gradient becomes nonuniform; there is a higher gradient in the high resistance region than in the rest of the dielectric. Breakdown occurs first in this layer by the electronic process described in the theories of von Hippel and Frohlich. This change of potential distribution occurs the more quickly the higher the conductivity. The value of conductivity at which an observable reduction in breakdown strength due to this cause first appears depends upon the chemical compoaition of the dielectric and of the electrodes. Several peculiarities of behavior are described in the paper, such as unusual characteristics of the power factor–temperature curves. These are not inconsistent with the process depending on high resistance layer formation by electrolysis, though they do not require this explanation. Both the electronic and the electrolytic effects should occur in some degree. It is a question as to which predominates in determining the breakdown behavior in a given material. The experiments of Lindsay and Berberich are valuable steps in the direction of understanding the factors involved in the breakdown of ceramics at high temperatures.

H. A. Frey (Locke Insulator Corporation, Baltimore, Md.): The Lindsay and Berberich paper presents some very interesting data on a subject about which all too little is known at present. Many of the characteristics reported by Lindsay and Berberich check in a general way our experience in this field, some of which has been reported in a previous paper.[1] There are, however, several points of difference between our respective data.

In the first place Lindsay and Berberich did not find any variation in the resistivity of the materials with varying voltage gradient. In our investigation we have found that most of the materials showed a reduction in apparent resistivity with increasing voltage gradient. It is quite possible that this difference between the two sets of data is occasioned by the voltage ranges over which the investigations extended. In the Lindsay and Berberich investigation, resistivity was measured over the ranges of 100 to 500 volts, and on specimens 0.125 and 0.25 inch thick. The gradients were therefore of the order of from about 0.4 volt per mil to 4 volts per mil. In our data gradients of much greater magnitude were involved running up to about 70 volts per mil on high voltage porcelain. In our data there was considerable indication that at the extremely low-voltage gradients there was a tendency for the curve to flatten out which would account for the measurement of nearly ohmic resistance at the gradients used by Lindsay and Berberich.

The resistances measured by Lindsay and Berberich for high-voltage porcelain at the lower temperatures are considerably lower than those measured by us. At the higher temperatures there is a reasonably good check between the resistivities determined in the two investigations. At 60 degrees centigrade, however, our resistivity measurements are approximately ten times those of Lindsay and Berberich for high-voltage porcelain and for similar voltage gradients. We do not know how to account for this difference in measured resistivity. It is possible that the resistivities obtained by Lindsay and Berberich may have been influenced by surface leakage. I would like to ask the authors whether or not guard rings were employed on the test samples to eliminate the possible errors occasioned by surface leakage.

A third point of apparent disagreement lies in the ultimate breakdown strengths. At high temperatures the Lindsay and Berberich paper shows breakdown strengths for high-voltage porcelain several times that indicated by our data. At low temperatures the reverse is true, our data indicating dielectric strengths higher than that measured by Lindsay and Berberich.

I am unable to explain the disagreement at low temperatures. At high temperatures, however, I believe that the disagreement is largely one of method of measurement. Lindsay and Berberich have pointed out that their measurements were made using the American Society for Testing Materials one minute step-by-step testing procedure. At the higher temperatures, the time required for breakdown to occur from the beginning of the thermal drift period may be quite long. In our data we have made some measurements where the time interval at a constant voltage, prior to breakdown, has been as much as 112 minutes. We also know of some practical field experiments where the time required to breakdown has been as much as $4^1/_2$ hours. Obviously with a one minute step-by-step test, these slow drifting failures would not be detected so that the measured breakdown strengths would be substantially higher. We believe that this probably accounts for the difference in breakdown strengths between our data and the Lindsay and Berberich data. We therefore feel that the data obtained by one minute step-by-step method must be used with caution in practical designs involving high temperature applications.

The theoretical discussion advanced by the authors is quite interesting and may well stimulate further work in this field.

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E. W. Lindsay and L. J. Berberich: We wish to thank Murphy and Frey for their informative and illuminating discussions. The information contained in them should make valuable additions to the paper. A number of questions were raised in both of these discussions which appear to require further comment.

Referring first to Murphy's remarks, we think the suggestion that electrolytic phenomena play an important role in high temperature breakdown appears to be quite plausible when d-c voltages alone are considered. As a matter of fact, considerable evidence of electrolysis and ion migration is shown in the lower part of Figure 10 of the paper in the form of discolorations produced upon application of a d-c voltage on the ceramic body at elevated temperatures. With a-c voltages, however, no evidence of electrolysis was found, although it may have taken place to such a slight extent that it remained undetected. In any case, with a-c voltages, the suggestion that electrolytic phenomena play a role loses some of its appeal. If electrolysis played a dominating role, then one would expect appreciably lower values of dielectric strength with direct current than with alternate current. The data in the paper, however, show that d-c and a-c peak values of dielectric strength are not greatly different. Thus, it appears that factors other than electrolysis enter into the breakdown mechanism.

Referring now to Frey's comments, most of the differences observed in his and our results can be ascribed to differences in methods of test used. He is correct in ascribing the lack of variation of resistivity with voltage to the low voltage gradients used in measuring resistivity. We also found deviations from Ohm's law in all of the bodies investigated at higher gradients. It is customary, however, to measure resistivity at gradients less than 10 volts per mil in order to avoid any temperature rise within the material due to I^2R heating. Frey purposely used much higher gradients in his measurements, and lower values of resistivity at the higher gradients are to be expected.

Frey points out that our resistivity values for high-tension porcelain in the low temperature region are lower than values obtained in his laboratory. This could be due largely to differences in composition of the samples and to a lesser extent to surface leakage. It is true that we did not use guard rings because they would have unduly complicated our apparatus which was designed to go over a much wider temperature range than that covered by Frey. Surface leakage may have been a factor at room temperatures during periods of high humidity, but as the temperature is raised, the humidity is reduced so rapidly that we would expect very little effect due to this cause at temperatures even as low as 60 degrees centigrade at the low voltage gradients that were used.

Resistivity variations of the order 10 to 1 are not uncommon among samples from several batches of supposedly the same composition. Therefore, it is not at all improbable that most of the differences referred to by Frey could have been due to slight compositional variations. Furthermore, since resistivity values are influenced by so many variables, several of which have just been mentioned, no high accuracy as to absolute values are to be expected. We do wish to emphasize, however, that considerable significance can be attached to the relative values in the three types of bodies investigated in this work, for the same procedure was used in all cases.

The differences in breakdown strengths to which Frey refers can be ascribed very likely to the differences in methods of test used in the two laboratories. This is particularly true at high temperature as he has already explained. We would expect lower values in the type of test he used where the voltages are applied for a much longer time than is the case in the ASTM one minute step test. It is questionable, however, whether the method of test used by Frey has much more practical significance than the one we used. In his tests, the voltage at which thermal instability arises is determined, among other things, by the dimensions and the rate at which heat can be dissipated from the sample. Therefore, test results on disk samples might be quite different from those on an insulator used in practice having entirely different dimensions and heat dissipating properties. Both types of tests have their merits and we agree that Frey's type of test may have greater practical implications. Again we wish to emphasize that too much significance should not be attached to the absolute value of the breakdown strength, for such values can be influenced by many factors. The relative values, however, are significant and show without question the superiority of the zircon and alumina porcelains over high tension porcelains for high temperature applications. We believe also that the maximum temperature at which a given voltage can be safely applied to a ceramic insulator is closely related to the transition temperature for that body.

In closing, we wish to call attention to several difficulties that we have had with nomenclature. The first has to do with the name of the most commonly used porcelain in the electrical industry, namely, "high tension" or "high voltage" porcelain. This name might indicate that this type of ceramic is better for high voltage applications than the other bodies described. The paper shows, of course, that this is not true, for the other bodies will withstand even higher voltages, particularly at the higher temperatures. The name "high-tension porcelain" may therefore cause some confusion. To avoid this confusion, the name "silica porcelain" could be assigned to this ceramic since it contains a major proportion of silica as Table I of the paper shows. This also would be more consistent with the names assigned to the other two ceramics investigated. In view of the growing importance of the other ceramics, we believe some consideration should be given to the name suggested to avoid confusion in the future.

The second difficulty has to do with the so-called "thermal region of breakdown". Frohlich has suggested to us, in a private communication, that the word "thermal" should be avoided in referring to the region of breakdown where the breakdown strength decreases with increasing temperature. We heartily agree with him and the only reason we used the term at all is because of the lack of a better term and also because the term has been used for many years, particularly since K. W. Wagner developed his thermal theory in 1922. As Frohlich points out, there is now considerable evidence that the decrease of breakdown strength with increasing temperature may take place without a rise in temperature over the surroundings of any part of the sample under test. The decrease in breakdown strength with increase in temperature, as both Frohlich and von Hippel have shown, can be accounted for by purely electronic considerations, which involve no I^2R heating in the sense that Wagner has treated the problem. This does not mean that a thermal rise is absent when breakdown strength decreases with increasing temperature, but that it may be absent. Therefore, to avoid confusion, perhaps the term "high temperature region of breakdown" should be substituted for the term "thermal region of breakdown" when reference is made to the region above the transition temperature. For the region below the transition temperature, the term "low temperature region of breakdown" is suggested. Some better terminology is obviously desirable and perhaps someone can invent a more satisfactory one than we have suggested.

A New Reactance Distance Relay

C. G. DEWEY
MEMBER AIEE

J. R. McGLYNN
APPLICATION PENDING

THE high level of industrial and commercial activity has created a demand for electric power greater today than at the peak of wartime production. Installation of additional generating capacity and new transmission lines has been unable to keep up with this growth in power demand. This has led to transfer of additional power over existing lines, thus making continuity of service more vital than ever.

Protective relay characteristics necessary to maintain the greatest continuity of service are high speed clearing of faults, selectivity to avoid false tripping, and insensitivity to power swings.

Over the past 15 years distance relays operating on reactance or impedance have been most widely used in providing this type of protection. When these were supplemented with carrier pilot channels, high-speed tripping was obtained for faults anywhere in the protected section.

The transfer of additional power over existing lines has caused a corresponding increase in the number and severity of power swings, particularly on the longer lines. To cope with this problem greater phase angle discrimination was required in a distance relay. For this reason the mho relay[1] was designed in which this characteristic is inherent. For over two years mho relays have been used for protection of long or heavily loaded lines[2] because they are less liable to tripping on power swings than the conventional reactance or impedance relays. However, there are two limitations to the application of mho relays on short lines.

1. The mho units are not accurate if the fault voltage is less than about eight volts, phase to phase, at the balance point usually two to three secondary ohms minimum.

2. On short lines arc resistance increases the impedance so that the fault may appear to be in the next zone.

Because of these limitations, reactance relays have been applied to short lines.

While the problem of power swings is more common in the protection of long lines, it may also be a serious factor on short lines under certain circumstances. For instance, the short line section may lie between two longer sections so that it contains the electrical center of the system. Under these conditions the starting unit of the present reactance relay is susceptible to operation because it does not have the same reach on phase-to-phase faults as on balanced conditions. To eliminate this difference a new distance relay has been developed for the protection of short lines.

This new relay retains the ohm unit of the present reactance relay which, due to its high accuracy, plus and minus 2 per cent, can be set for first zone tripping up to 90 per cent of the protected section. However, the single-current 2-voltage starting unit has been replaced by a mho starting unit.[3] To obtain the desired characteristics, this unit is energized by the proper quantities, phase-to-phase voltage and the 2-phase currents. These are the fault voltage and the currents in the faulted phases on a phase-to-phase fault.

Description of Relay

In developing this new reactance relay, it seemed logical to follow the pattern of the mho relay, that is, the combination of three single-phase distance relays and a separate timing relay per 3-phase terminal. The following paragraphs give a brief description of the operation of the new terminal.

Paper 48-178, recommended by the AIEE relay committee and approved by the AIEE technical program committee for presentation at the AIEE North Eastern District meeting, New Haven, Conn., April 28–30, 1948. Manuscript submitted February 13, 1948; made available for printing March 12, 1948.

C. G. Dewey and J. R. McGlynn are both with the General Electric Company, Philadelphia, Pa.

Under normal conditions the ohm unit may be open or closed depending upon the reactive component of the load impedance. The mho unit will be open because it is provided with voltage restraint.

On a fault in the nontripping direction, the mho unit will remain open because of its directional properties.

If a fault occurs in the protected section within the reach of the ohm unit setting, the ohm unit closes instantly, and may even be already closed, and the mho unit operates to set up the trip circuit immediately. The mho unit is provided with memory action so that faults near the bus may be cleared quickly and positively.

When a fault occurs beyond the first zone setting of the ohm unit, its contacts will be open so that tripping cannot occur when the mho unit closes. In this case the mho unit energizes the timing relay. After a predetermined time the reach of the ohm unit is extended to the second zone through the operation of its auxiliary element. If this change in reach is sufficient to allow the ohm unit to operate, tripping will now occur. When the fault is beyond this ohmic reach, the timing relay continues to operate for an additional preset time after which tripping occurs independent of the ohm unit operation. Should the fault be cleared at any time during this period, the mho unit contacts open and the timing relay resets.

Ohm Unit

The ohm unit is shown schematically in Figure 1. The four laminated poles surround the inner core with a small annular air gap between. A hollow aluminum cylindrical rotor, secured to the shaft carrying the moving contact, turns in this gap. The contacts, protected from excessive forces by a friction clutch, are held in the open position by a light spiral spring which also serves as a lead-in for the contact current.

This induction cylinder unit produces a

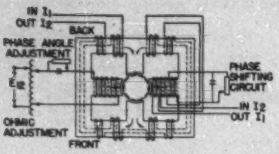

Figure 1. Schematic diagram of the ohm unit connected between phases 1 and 2

substantially constant torque at all points in the cycle. Also its torque is dependent on rate of change of current, rather than magnitude, thus making it relatively insensitive to offset currents. Because of these two factors, high-speed operation can be obtained without danger of excessive overreach.

The principle of operation of the reactance ohm unit can be explained most simply by considering first a single-phase system and then showing how the same principle may be applied to a 3-phase system. In Figure 2 is shown a single-phase system in which it will be assumed that a reactance unit is desired at A which will close its contacts for all faults between conductors occurring to the left of P, for example at F.

For simplicity it will be assumed that there is no impedance in the fault. It can be seen at once that

$$E/I = Z_{AF} \tag{1}$$

where E and I are defined from Figure 2 and Z_{AF} is the impedance of the loop, AF.

Operation is then required when

$$E/I < Z_{AP} \tag{2}$$

where Z_{AP} is the impedance of the loop, AP, or, since the reactance principle is to be used, when

$$E \sin \theta / I < X_{AP} \tag{3}$$

where θ is the angle by which the current lags the voltage.

A unit is then required which will be just ready to operate when

$$E \sin \theta / I = X_{AP} \tag{4}$$

and so constructed that a decrease of the ratio E/I will cause operation. Such a unit is indicated schematically in Figure 3.

This figure represents the operating element of the induction cylinder unit.

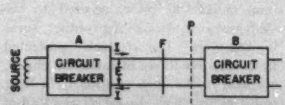

Figure 2. Simplified single-phase transmission line

As in an induction motor, it is necessary to have a difference in time phase of the magnetic flux in alternate poles in order to produce a torque on the rotor. The front and back poles are called polarizing poles.

The flux in the right-hand or current pole is made to lag its current about 90 degrees so that in conjunction with the polarizing poles it produces clockwise torque

$$T_I = K_1 I^2 \tag{5}$$

where K_1 is a proportionality constant.

The left-hand or potential pole has its coil tuned to phase resonance[4] so that its current is in phase with E, thus it works with the polarizing poles to produce a torque

$$T_E = K_2 EI \sin \theta \tag{6}$$

where K_2 is a second constant. When $\sin \theta$ is positive this torque is counterclockwise to oppose the current torque.

If then

$$K_1 I^2 = K_2 EI \sin \theta \tag{7}$$

no net operating torque will be produced. Also, a decrease in the ratio E/I will cause a net clockwise torque.

From equation 7

$$K_1/K_2 = E \sin \theta / I \tag{8}$$

If then the constants are so chosen that

$$K_1/K_2 = X_{AP} \tag{9}$$

the net torque is zero when a fault occurs at P in Figure 2. Thus a unit has been described which fulfills the requirements set forth for the single-phase system.

It has been well known since 1931 that for a 3-phase system, a unit operating on delta current and voltage will operate in the same manner for all types of interphase faults at a given location.[5] While the delta voltage can be obtained readily in most cases, quite often the delta current is not available without using auxiliary current transformers. However, it is possible to obtain the same effect in the relay by means of two concentric coils, each carrying one of the star currents.

By this means the principles described for the single-phase system are made applicable to a 3-phase system. It should be noted that the reactance measurement is phase-to-phase or twice the phase-to-neutral reactance which is used for convenience in application ratings.

The ohm unit as actually used differs slightly from the foregoing description in that each of the two polarizing coils indicated in Figure 3 is replaced by two coils, one on each side of the pole as shown

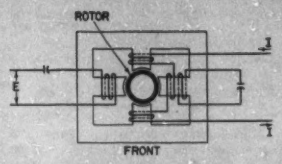

Figure 3. Schematic diagram of a single-phase ohm unit

in Figure 1. Also, adjustable resistors are connected in parallel with the capacitors. These resistors provide the required factory adjustments for the unit and also help to damp out undesirable transients which may be present when a fault occurs.

Control of the ohm unit reach is afforded by means of a tapped autotransformer in the potential circuit. Reducing the tap setting reduces K_2 in equation 9, thus increasing the reach.

At low currents the ohm unit reach is reduced because of the torque required to overcome friction and the lead in spiral. A typical accuracy curve under phase-to-phase fault conditions is shown in Figure 4. For 3-phase fault conditions the curve is displaced to the right because the two currents are not in phase.

Mho Starting Unit

The mechanical construction of the mho unit is similar to that of the ohm unit except that it is provided with two circuit-closing contacts. As indicated in Figure 5, this unit is also energized by phase-to-phase voltage and the two phase currents. The mho unit has been described previously,[1] so its characteristics will be set forth only briefly here.

The side poles are energized by potential to produce the polarizing flux. The front pole is energized by current to produce a directional torque dependent on the phase angle between this current and the polarizing voltage. Control of the maximum torque angle is afforded by an adjustable resistor in the phase shifting circuit.

The back pole is energized by potential

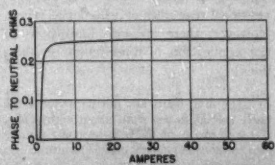

Figure 4. Variation of ohm unit reach with current magnitude for phase-to-phase faults

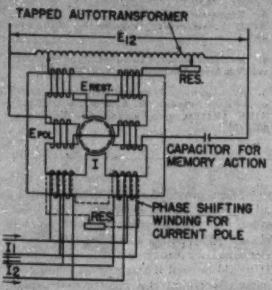

Figure 5. Schematic diagram of the mho starting unit connected between phases 1 and 2

from an autotransformer to produce a voltage restraint torque. The tap setting of this autotransformer determines the amount of voltage restraint, and hence the *EI* torque required to overcome this restraint. The reach of the unit is thus controlled by a simple field adjustment.

Timing Unit

The timing unit is provided in a separate relay. The operating magnet is a curved solenoid called a Rotonoid, having an armature which rotates through 180 degrees.

This Rotonoid, shown in Figure 6, winds up a spiral spring to drive the main shaft which carries a circular calibrated scale and two adjustable insulated cams for operating the contacts. Connected to this main shaft through a set of spur gears is a magnetic damping element for regulating the speed of operation. This element, shown in Figure 7, consists of a copper cylinder rotating in an annular air gap across which exists a permanent magnet field. The relative position of the cylinder in the air gap determines the amount of damping obtained. To insure good alignment all the parts are mounted on two similar parallel plates, following the pattern of good watch and clock design. Reliability is assured with this design which, employing high torque on the damping element and only one set of gears, is subject to a minimum amount of friction.

Additional Elements

In addition to the basic units described, auxiliary elements are provided to insure co-ordinated operation of the relay terminal. Since the mho starting unit is polarized by the fault voltage, it resets somewhat slowly when a second or third zone fault is cleared elsewhere. To insure against inadvertent tripping during this resetting interval, the first zone trip circuit is opened about 0.03 second after the mho unit operates. As this might prevent first zone tripping on faults near the end of the ohm unit reach, an auxiliary element restores the trip circuit if the mho unit remains closed for 0.015 second after the ohm unit closes. The one cycle delay which this may cause in clearing first zone faults has relatively little effect on the over-all protection since it is added only for faults in about the last 10 per cent of the zone on which the ohm unit is relatively slow.

A target is provided in each distance relay to show which phases are affected, while targets in the timing relay indicate in which distance zone tripping occurs. A seal-in element is provided in the timing relay to protect all the trip circuit contacts.

Ratings and Settings

Three different ratings of ohm units are available for covering various lengths of lines. These are 0.25 to 2.5; 0.5 to 5; and 1 to 10 ohms phase to neutral. In selecting the rating for a particular application, the highest minimum ohms should be used which will permit the required first zone setting. This insures a high operating torque level since the first zone potential tap setting will always be 50 per cent or more with the 2:1 steps

Figure 6. The Rotonoid operating magnet of the new timing relay

Figure 7. The magnetic damping element of the new timing relay

of minimum ohmic ratings available. In each case the upper part of the ohmic range is intended only for relatively high second zone settings which may be desirable.

The same mho starting unit is used with all three ohm unit ratings and has a range of 2.5 to 20 ohms phase to neutral. The maximum torque angle may be varied over a range of 60 to 75 degrees lag by means of an adjustable resistor.

The reach of both units is adjusted by means of taps on an autotransformer in the potential circuit. With this arrangement, the first zone setting may be made in steps of no more than one per cent without any mechanical adjustment of the unit.

The two insulated cams operating the contacts of the timing relay can be adjusted individually. A range of time adjustment from 0.15 to 3.0 seconds is provided.

Testing

Since only one voltage is used in the operation of the new relay, a single-phase source is sufficient for making all tests. The test box and reactor now used for testing distance relays, provide the simplest and most accurate means of making settings. The reactor is used as the fault impedance in combination with a tapped resistor to simulate arc or line resistance in the test circuit. The use of this equipment obviates a phase shifter and meter accuracy since the reactor is used as a standard of comparison. This method of testing has the further advantage that it matches closely the transient conditions encountered during a fault.

Summary

The new reactance relay has the inherent high speed and accurate distance measurement characteristics of the present relay. In addition, it has the following advantages:

1. The mho starting unit reach is the same for all faults involving more than one phase.

2. The new timing relay provides more accurate and consistent timing. It has a radically new operating magnet and employs a powerful magnetic damping element both of which reduce the gearing to one set of gears.

3. Only a single phase supply is required for making tests.

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2. Application of the Ohm and Mho Principles to Protective Relays, **A. R. van C. Warrington.** AIEE Transactions, volume 65, 1946, pages 378–86.

3. Discussion by **W. R. Brownlee** of reference 2. AIEE Transactions, volume 65, 1946, pages 490–1.

4. *American Standard C42-1941, 05.05.400* American Standards Association. (New York, N. Y.)

5. Fundamental Basis for Distance Relaying on 3-Phase Systems, **W. A. Lewis, L. S. Tippett.** AIEE Transactions, volume 66, 1947, pages 694–708.

Discussion

C. E. Asbury (The Commonwealth and Southern Corporation, Jackson, Mich.): The redesign of the starting unit for the reactance-type distance relay to incorporate the mho circle characteristic should extend the application of reactance relays to short lines where power swing and load ability considerations are becoming more important. As pointed out by the authors, the old starting units used for reactance relays varied over a wide range in characteristics since they responded differently to phase-to-phase and 3-phase faults. This made it necessary to set the relay one and a half to two times as sensitive to 3-phase load current as should be necessary with the mho-type starting units.

It appears that no fundamental changes have been made in the design of the reactance or mho units used previously; but these units have been installed in one case, resulting in a reactance relay having improved starting unit characteristics. The timer used previously with the distance relays has been completely redesigned, and it would be helpful to know more concerning its connections and operation with the new type reactance relay. Reference is made to additional elements in the new timing relay which apparently are necessary to co-ordinate the timing between the mho and ohm units to prevent possible incorrect relay operation. It appears that this may result in additional contacts in the trip circuit, which is undesirable. One of the advantages of using mho-type element is its inherent directional characteristics making it unnecessary to have additional directional contacts in the trip circuit. However, if additional auxiliary relays must be added to the distance relay in order to achieve proper co-ordination of the mho unit with the ohm unit, the advantages of this simplicity are lost. It is especially undesirable to have relay back contacts in tripping circuits. It would be helpful if the authors could include a simplified schematic wiring diagram of the new reactance relay and its associated timer, showing the over-all tripping arrangement.

It is hoped that further developments will make it possible to co-ordinate the starting and ohm units to take full advantage of the simplicity in the tripping circuits and keep the number of contacts in the tripping circuit to a minimum.

The authors state that the seal-in elements are provided in the timing relay for all tripping circuit contacts. We understand that consideration has been given to changing this design and moving the seal-in elements back into the individual relay cases. It would be well if the authors could comment concerning this change and explain the advantages of this new arrangement. We understand that the new timer coils are intermittently rated, which we believe is undesirable, especially for installations where the relay potential supply is from line side potential devices that are subject to being de-energized for long periods of time. We prefer a timer having a continuous rating. It would be helpful if the authors would comment concerning this rating and point out why it would not be preferable to design a timer having a continuous rating.

Since the new relay is a single-phase unit, it should be possible to simplify the method of testing, which is desirable. The authors point out that test reactors may be used for setting the relay. One point that should be kept in mind when using the test reactor is not to exceed its current rating, since this may produce saturation. When the reactor saturates, poor wave form results in the test voltage and incorrect calibration of the relay may result.

Feeder Voltage Regulator Accuracy Standards

W. E. BIRCHARD
MEMBER AIEE

Synopsis: New standards* for the accuracy of feeder voltage regulator control devices will provide much needed performance specifications. A working knowledge of their provisions will be an important aid for distribution engineers.

Electrical distribution systems are usually designed and operated to maintain the consumers' voltage within specified limits. These limits are determined either by state commission rules or by utility engineering standards for good service. Variation in consumers' voltage results principally from three causes:

1. Regulator band width.
2. Regulator control errors.
3. Line drop (including transformer drop).

On a distribution system the difference between the maximum voltage of the first consumer and the minimum voltage of the last consumer, over a period of time, is the sum of items 1, 2, and 3 above. (The first consumer is the one electrically nearest the supply, and the last consumer is the one electrically farthest from the supply.) These maximum and minimum voltages must be within the specified limits mentioned before. Thus, if either the regulator band width or the regulator control errors are decreased it is permissible to increase the line drop in either of two ways, by increasing the load on the feeder, or by extending the length of the feeder. A third possible benefit is to maintain the same maximum voltage for the first consumer and increase the minimum voltage of the last consumer by the amount of decrease in band width or control errors and maintaining a higher average feeder voltage.

It is apparent that decreasing regulator control errors and decreasing band width are equally important, and that all three possible benefits of either or both have a real economic value.

Need for Accuracy Standards

Many factors such as variations in temperature, frequency, and load affect the accuracy of regulator control devices. The over-all error of the several control devices of an automatic feeder voltage regulator might be based on any of innumerable possible combinations of various ranges of these factors. Obviously, it is very important to both utilities and manufacturers to have a single set of standards universally accepted by both. The standards should list the sources of error to be included and specify procedures for determining the errors. They should also specify how the errors are to be combined in determining the over-all error and define accuracy classes. Ranges of variation of temperature, frequency, and so forth, recognized by any standards will necessarily be somewhat arbitrary, but if they have been chosen to cover the variations normally encountered in actual practice the advantages of having a single set of universally accepted standards will outweigh the disadvantages of not having specifications tailored exactly to the conditions encountered in a particular utility system. Without such standards, statements of regulator control accuracy have been cumbersome and extreme care has been required to avoid misunderstandings between manufacturers and utility engineers.

When the accuracy standards have become established, distribution engineers can readily select regulators having accuracies to suit their needs and design their systems to take full advantage of the accuracies selected. Generally, it will be found economical to obtain high accuracy regulators and use the lower cost line construction permitted by trading line drop for regulator control errors. If a distribution engineer has not known how accurate his regulators are he may have been over-conservative and allowed for more control error than necessary, thus requiring needlessly expensive line construction to keep his line drop low. Of course, he should not accept accuracy guarantees blindly, but should have a working knowledge of the accuracy standards so that he can determine whether his operating conditions fall within the ranges of variables recognized by the standards. Generally they will, but some exceptions may occur for which additional allowances should be made.

Proposed ASA Standards for Regulator Control

The proposed ASA standards for "Accuracy of Control Devices for Induction Voltage and Step Voltage Regulators," meet the requirements outlined before. They classify the errors into two groups:

1. Errors occurring in the potential supply and voltage regulating relay with its ballast.
2. Errors occurring in the current supply and line drop compensator.

Since the recognized sources of error are not the same for both groups, and the test procedures are different, the two groups will be discussed individually.

Potential Supply and Voltage Regulating Relay

Although errors occurring in the line drop compensator are determined sepa-

* Standards for "Accuracy of Control Devices for Induction Voltage and Step Voltage Regulators," have been prepared by a working group of the AIEE transformer subcommittee and recently submitted to American Standards Association (ASA) Sectional Committee C57 for inclusion in the American Standards for Transformers, Regulators, and Reactors.

Paper 48-177, recommended by the AIEE transformer committee and approved by the AIEE technical program committee for presentation at the AIEE North Eastern District meeting, New Haven, Conn., April 28-30, 1948. Manuscript submitted February 16, 1948; made available for printing March 18, 1948.

W. E. Birchard is with the motor engineering division, General Electric Company, Pittsfield, Mass.

rately, portions of the compensator circuit are usually included as part of the voltage circuit even with the compensator set on zero. These compensator elements function as part of the voltage regulating relay ballast. Compensators for modern single-phase regulators are usually provided with phase-shift networks to obtain correct line drop compensation on 3-wire 3-phase systems without interconnecting the control circuits. Since the impedance characteristics of the compensator may be different for the various phase-shift connections, it is necessary to determine the errors occurring in the voltage regulating relay and its ballast for each of the phase-shift connections.

Test Conditions. The accuracy of the potential supply and voltage regulating relay with its ballast is determined under the following specified conditions:

1. Both the resistance and reactance elements of the compensator are set on zero.
2. The applied voltage is to have a constant wave form with a deviation factor not exceeding 0.03 as defined in ASA *C-42*, paragraphs 10.95.420.
3. For 3-phase regulators, the three phase voltages and the three line currents are to be balanced.
4. The band width of the voltage regulating relay is excluded in determining accuracies.

The potential supply is usually tested separately from the voltage regulating relay and its ballast, and then the errors are combined as explained under Summation of Errors.

Setting the compensator on zero eliminates all possibility of introducing compensation voltages which would be extraneous variables for this series of tests. The temperature and frequency characteristics of the voltage regulating relay and ballast circuit of modern regulators with which the writer is familiar are not affected by the setting of the compensator. For control circuits where this is not true more representative results would be obtained with mid-range compensator settings of resistance and reactance; however, this would require more elaborate test specifications to eliminate compensation voltages resulting from load current.

Condition (2), constant voltage wave form, is believed to be representative of most power systems. Condition (3), balanced voltage and current, is often not obtained in practice. However, the degree of unbalance is generally held to a minimum, and both test specifications and interpretation of results would be greatly complicated if various combinations of unbalance were included.

Exclusion of band width simplifies the Standards, but from the practical application viewpoint it should not be ignored. The band width of the modern voltage regulating relay and ballast combination with which the writer is familiar is practically independent of temperature and frequency variations over the ranges specified by the standards, but this is not an inherent property of all voltage regulating relays. If the band widens with temperature change the widest band encountered in the range of operating temperature should be recognized in designing a distribution system. Admittedly, effective band width is a controversial subject involving consideration of the duration of voltage fluctuations, but any such transient effects will be superposed on the steady state response of the voltage regulating relay.

It seems to the writer that for testing the potential supply the standards should also either specify that the regulator supply voltage is to be held constant at rated value throughout the tests, or that the output voltage is to be held constant, at rated value, since these conditions may have some effect on potential winding errors. Both the supply and output voltages vary in actual service, but since line drop compensation is generally utilized it is believed that for any typical installation the regulator output voltage will vary more than the supply. Therefore, it is recommended that the supply voltage be held constant as a test condition.

Reference Values. The per cent error is based on the following reference conditions:

1. Regulator on its neutral position.
2. 120 volts in the potential control circuit.
3. Rated frequency.
4. Zero load current.
5. Ambient temperature plus 25 degrees centigrade.

Sources of Error. The recognized sources of error, in addition to characteristics of the devices, and the ranges to be considered are listed below.

1. Regulator position; maximum raise to maximum lower.
2. Load current; zero to rated current in the series winding at a power factor of 80 per cent lagging.
3. Ambient temperature; minus 30 degrees centigrade to plus 40 degrees centigrade.
4. Frequency; plus and minus 0.25 per cent.

The only effect of the first two sources of error on the voltage regulating relay and its ballast is that of load current in raising the ambient temperature of the control devices above the ambient of the air surrounding the regulator. For regulators equipped with potential transformers the temperature effect of load current is the only result of error sources (1) and (2) which affects accuracy of the potential supply. For regulators having built-in potential supply windings, in general, the effective voltage ratio between the main windings and the potential supply windings is affected by regulator position and by both the magnitude and power factor of the load current. The power factor is defined as "the load power factor of the circuit at the output terminals of the regulator," and for single-phase regulators this involves testing at three power factors. On single-phase and 3-phase 4-wire systems the power factor for the regulator is the same as for the system; the load current lags the output voltage by 36.87 degrees. However, on 3-phase 3-wire systems the power factor for a single-phase regulator is greater or less than that of the system depending upon phase rotation and the way the regulator is connected into the circuit; the load current lags the output voltage by 6.87 degrees or by 66.87 degrees.

The ambient temperature is specified as that of the air surrounding the regulator. Since the control devices are usually tested separately from the regulator, the temperature rise of the control cabinet above the ambient of the surrounding air must be determined at no load and with full load on the regulator. Then when testing the voltage regulating relay and its ballast the temperature rise of the cabinet at full load must be added to the upper end of the temperature test range, and the lower limit of the temperature test range should be adjusted by the cabinet rise at no load. The lower end of the range will generally be affected very little because at no load the cabinet temperature will be nearly the same as that of the surrounding air. For example, if at full load the temperature of the air inside the control cabinet is found to be 10 degrees centigrade above the ambient of the air surrounding the regulator, and at no load 2 degrees centigrade above the surrounding air, then the temperature range for testing the voltage regulating relay separately would be minus 28 degrees centigrade to plus 50 degrees centigrade instead of minus 30 degrees centigrade to plus 40 degrees centigrade. The effect of regulator load on the temperature of potential supply windings or transformers should be included in a similar manner. Calculated

temperature errors based on measured resistances and measured temperature rises above ambient should be satisfactory for potential supply windings and transformers.

Only a small variation in frequency is recognized. Plus and minus 0.25 per cent is plus and minus 0.15 cycle for a 60-cycle system. Probably the instantaneous frequency fluctuation on some small systems is greater than this, but presumably the larger systems hold within these limits. Frequency variation may cause an error in voltage regulating relay and ballast combinations, but not in potential supply windings or transformers.

It is, of course, not intended that the manufacturer shall make a complete set of accuracy tests on each regulator; such a procedure would increase the cost prohibitively. Therefore, the manufacturer must make due allowance for manufacturing variations in individual units.

Summation of Errors. Errors in the potential supply and voltage regulating relay are determined for each of the connections of the compensator phase shift network. The zero phase-shift connection is used on single-phase and 4-wire 3-phase systems; the errors determined for the voltage regulating relay and ballast for this connection should be combined with the errors in the potential supply for a load power factor angle of 36.87 degrees (0.80 power factor). The 30-degree phase shift connections are used on 3-wire 3-phase systems. The relay and ballast errors for the 30-degree leading phase shift connection should be combined with the potential supply errors for a regulator load power factor angle of 6.87 degrees. Likewise, the errors for the 30-degree lagging connection should be combined with those for a regulator load power factor angle of 66.87 degrees.

The standards define the "per cent plus error" of the potential supply and voltage regulating relay as "the sum of the plus errors" and likewise the "per cent minus error" as "the sum of the minus errors" due to variations in regulator position, load current, ambient temperature, and frequency as outlined under Sources of Error. This seems straightforward enough at first glance, but further consideration shows that definitions of "plus errors" and "minus errors" are needed for proper classification. For example, if a potential transformer or potential winding supplied a voltage of 120.5 when it should have been 120.0, the error would, in the absence of definition, probably be considered plus 0.5 volt. Likewise, if 120.5 volts were required to balance a voltage regulating relay when it should have balanced at 120.0 volts, the error would probably be considered plus 0.5 volts. Yet it is apparent that the combination of this particular potential supply and relay would hold correct regulator output voltage; in other words, these two errors cancel each other.

In order that consistent and practical results may be obtained in applying the accuracy standards the writer proposes the following definitions:

Errors causing the regulator to maintain a voltage level greater than the reference level are plus errors.

Errors causing the regulator to maintain a voltage level less than the reference level are minus errors.

If these definitions are accepted the error in the potential supply of the foregoing example would be "minus" because with it the regulator would maintain lower than nominal line voltage to supply 120.0 volts to the control circuit. The voltage regulating relay error would be "plus" because the regulator would have to hold higher than nominal line voltage to balance the relay. Combining these two errors results in zero net error which is in agreement with actual performance if the conditions producing them occur simultaneously. As indicated before, plus errors and minus errors are not combined in determining the over-all error, but proper classification is needed because plus errors are added together and likewise minus errors.

Current Supply and Line Drop Compensator

The accuracy of the current transformer, line drop compensator, and associated circuits is determined in terms of the voltage errors produced by these devices in the potential circuit. They are tested in combination.

Test Conditions. The accuracy of the current supply and line drop compensator is determined under the following conditions:

1. Frequency approximately rated.
2. Ambient temperature approximately plus 25 degrees centigrade.
3. Voltage regulating relay adjusted to balance at approximately 120 volts.
4. Compensator elements set on mid-range marks.

Conditions (1) and (2) are intended to indicate that frequency and temperature are to be held constant throughout a test. No attempt is made to evaluate frequency and temperature errors in the compensator. The recognized range of frequency variation is small and reactive line drop is proportional to frequency; even with phase-shift networks the discrepancies between reactance compensation and actual reactive line drop due to frequency variation will be very small. Any attempt to evaluate temperature errors in the compensator would be too involved to be practical. To supply perfect resistance compensation, the resistance element must have the same temperature coefficient as the lines and transformers which it represents and it must be subjected to the same temperatures, or have a combination of temperature and temperature coefficient which produces an equivalent result. Since lines and transformers vary over different temperature ranges, and are represented in different proportions of the compensator settings on various systems, it is hopeless to try to design a compensator with an equivalent effective temperature coefficient. Furthermore, some regulators are installed indoors and some outdoors, and even for outdoor regulators the control cabinets do not follow exactly the same temperature cycles as lines and distribution transformer windings. Fortunately, the resistance drop usually represents only a small percentage of the voltage held and therefore discrepancies between resistance characteristics amount to an extremely small percentage of the total voltage.

The voltage regulating relay setting is not explicitly stated, but 120 volts is implied because this is the reference voltage for testing the relay and it is also the base value for converting voltage errors to percentage values. In most cases the exact voltage level setting of the relay will have only a secondary effect on test results because they are the differences between voltages required to balance the relay with and without current in the compensator. However, since these are vector differences, the level setting of the relay will have some effect.

The selection of the mid-range setting as the test position for the compensator is somewhat arbitrary, but it seems to be the most representative single setting. Since dial marking errors are excluded from the accuracy determination, results for most practical types of compensators should be nearly proportional to the setting at other settings.

Recognized Errors. Compensators for modern single-phase regulators are usually provided with phase shift networks to obtain correct line drop compensation on 3-wire 3-phase systems without interconnecting the control circuits. The current supply and line drop compensator errors

are to be determined for each phase shift connection. The procedure outlined below is for the zero phase shift connection. For the 30 degree lag phase shift connection the procedure is repeated except that the current in the current transformer primary is shifted 30 degrees lag with respect to the voltage regulating relay potential from the angles required for the zero phase shift connection. Likewise, the procedure is repeated for the 30 degree lead connection except that the current is shifted 30 degrees lead from the angles required for the zero phase shift connection.

The only recognized sources of error are variations in magnitude of the load current and power factor of the load current. Errors in the resistance element and in the reactance element of the compensator are determined separately and the errors for combined resistance and reactance compensation are calculated from the individual errors. In determining accuracy the standards include compensation errors resulting from errors in the angle of compensation voltage, but exclude dial marking errors. This is discussed later.

A calibrated value for the mid-range setting of the resistance element is determined by test as the difference between the voltages required to balance the voltage regulating relay with zero regulator load current and with rated load current at unity power factor. (The reactance element is set on zero.) The actual change in applied voltage required to balance the voltage regulating relay as the load current is changed from zero to rated value at 80 per cent power factor is then determined by test or equivalent calculation. The error in the resistance element for rated current is the difference between the actual change in balancing voltage for 80 per cent power factor load current and the theoretically correct change computed for 80 per cent power factor based on the calibrated value. To insure obtaining the proper sign for the error the standards might be reworded to indicate more specifically that the difference should be taken as the "actual change minus the computed change" in voltage.

Errors in the resistance element for 25, 50, and 75 per cent rated load current at 80 per cent power factor are also determined in the same manner as for 100 per cent rated current, except that the calibrated values are determined by interpolation from the calibrated value for rated current instead of by direct test.

Calibrated values for the mid-range setting of the reactance element are determined in the same manner as for the resistance element except that zero power factor load current is used. Errors in the reactance element for each of the four values of load current at 80 per cent power factor are determined by the same procedure as for the resistance element.

Errors in the combined resistance and reactance elements could be determined by a procedure similar to that used for the individual elements. However, this would involve certain complications not encountered when dealing with the elements individually, and objections have been raised. One complication is that errors in the magnitudes of the resistance and reactance compensation voltages change the effective impedance angle of the combination to something other than 45 degrees. Unless the current was adjusted to a corresponding angle in determining a calibrated value for the combination these magnitude errors would appear in the error determined by this method, and it is not the intent of the standards to include magnitude, or dial marking, errors. Another complication is that the calibrated value method would yield zero error for a combination of resistance and reactance elements having equal magnitudes of compensation voltage and equal but opposite angular errors, although the actual result of such a combination is that the individual errors are additive. For example, consider a resistance element which produces exactly 12 volts compensation at the midposition but has an angle of plus five degrees instead of zero degrees, and a reactance element of exactly 12 volts but at 85 degrees instead of 90. When tested individually in accordance with the standards the error of the resistance element is plus 0.68 volt and the error of the reactance element is plus 0.89 volt. If tested in combination by the same method the resulting error would be zero because the impedance angle of the combination is 45 degrees. The balancing voltage for the voltage regulating relay would be 1.40 volts too high, but this would not appear as an error because the calibrated value for the combination would be correspondingly high.

As a simple approximation to the error of the combination of resistance and reactance elements, the standards use the algebraic sum of the errors for the individual elements. For the previous example, the sum is 1.57 volts, which is 0.17 volt greater than the actual error of 1.40 volts. If the reactance element had an angle of 95 degrees its individual error would be minus 0.89 volt. The algebraic sum of this error and the plus 0.68 volt error of the resistance element of the example is minus 0.21 volt. This is less than the actual error of minus 0.31 volt for the combination. Although the algebraic sum of the individual errors is not as accurate as may be desired it does more nearly represent the actual error of the combination of resistance and reactance than either of the individual errors or their arithmetic sum. Possibly a better proposal will be developed with actual usage.

Errors Excluded. The calibrated value method excludes magnitude or dial marking errors. The two principal arguments for this exclusion are that a dial marking error is readily compensated for by simply moving the dial to a position which gives the desired compensation voltage, and that on most distribution systems the final compensator setting is determined by trial. Where resistance and reactance elements are used in combination the same arguments might be applied to excluding angular errors except that correcting for angular errors is more involved.

Although the dial marking errors are not included in the over-all accuracy determination, the standards specify that such errors expressed as a per cent of 120 volts shall not exceed the per cent error permitted for the accuracy class of the associated regulator. This limit on dial marking error applies to any position of either the resistance or the reactance element with rated current in the compensator.

Determination of Maximum Plus and Minus Errors. The maximum plus error and the maximum minus error of the current supply and line drop compensator is determined for each phase shift connection. This is simply a matter of selecting the maximum plus and minus errors from the twelve errors determined for each phase shift connection. Errors in the resistance element are determined by test for each of four current values and likewise in the reactance element for each of four current values. Then errors for the combined resistance and reactance elements are calculated for each of the four current values as the algebraic sum of the errors in the individual elements.

By recognizing the maximum error found in either the resistance element alone, or the reactance element alone, or the two in combination, the standards obtain a value representative of about the worst error likely to be encountered in actual service. If only the error for combined resistance and reactance were recognized each element might have large errors, yet high accuracy would be indicated if the errors in the individual

elements offset each other. Such an accuracy classification would be misleading for installations where either predominately resistance or predominately reactance settings are used.

Accuracy Classes

Three standard accuracy classes of control devices for feeder voltage regulators are defined in Table I.

The sum of the plus errors and the sum of the minus errors is determined for each phase shift connection. The maximum of these sums is the over-all per cent error.

It is the intent of the standards that for each phase shift network connection the sum of the plus errors shall be obtained by adding the plus error of the potential supply and voltage regulating relay to the plus error of the current supply and line drop compensator with associated circuits. The plus error of the potential supply and voltage regulating relay is the sum of the plus errors due to regulator position (maximum raise to maximum lower), ambient temperature, frequency, and load current. The plus error of the current supply and line drop compensator is the maximum of the plus errors determined for resistance compensation, reactance compensation, or combined resistance and reactance compensation. The sum of the minus errors for each phase shift connection is to be determined in a like manner.

Potential Test Terminal Burden

Feeder voltage regulator control panels are usually provided with test terminals to which instruments can be connected to measure regulator output voltage. The standards state that connecting a burden of 25 volt-amperes at 0.7 power factor shall not change the test terminal voltage by more than plus or minus 1 per cent, unless otherwise specified. However, this deviation is not included in the specification of accuracy.

Some recording instruments impose a burden greater than 25 volt-amperes, and there may be occasions when it will be desired to connect more than one recording instrument to the same terminals. Some engineers might object to applying a correction of even one per cent to recording instrument charts. In this connection it should be noted that even though the recording instrument causes a voltage drop of one or two per cent in the control circuit and the regulator maintains a correspondingly higher line voltage, the chart obtained will show the voltage which will be maintained after the instrument is disconnected. Usually this is the record desired and no correction is needed. Of course, if a simultaneous recording is made at some other point on the feeder the two charts will not be directly comparable because of the temporarily high voltage held by the regulator. In such a case to obtain charts representing conditions after the instruments are disconnected, the correction should be applied to the chart obtained at the remote point, and not to the chart obtained at the regulator test terminals.

Table I

Accuracy Class	Over-all Per Cent Error
Class 1	plus or minus 1
Class 2	plus or minus 2
Class 5	plus or minus 5

Testing Facilities

Testing technique is beyond the scope of this paper. It is merely desired to emphasize the importance of extreme care and precision in measuring the accuracy of class 1 or class 2 control equipment. Since the sum of the several errors must be less than one or two per cent, each individual error must be much less, and this is beyond the accuracy of most ordinary instruments. Generally special set-ups will be required, even in well-equipped laboratories. Otherwise, the errors indicated may be due more to errors in measurement than to errors in the apparatus tested.

Many instrument problems will be encountered. For measuring voltage, accuracy at least equal to that of a laboratory standard type of voltmeter, ordinarily used for calibrating portable instruments, is required, and better accuracy is desirable. While assignment of this type of instrument to such usage may seem extreme, it should be recognized that many of the errors to be accurately determined are smaller than those found in many good portable instruments. In measuring frequency errors, it will usually be desirable to check several points over a range of several cycles and determine errors for the specified range from a carefully plotted curve. Really accurate measurement of power factor, or power factor angle, will be new to many laboratories.

Unusual stability of voltage and frequency will be required in power sources, and this must be obtained without distortion of voltage wave shape. Some means for indicating precisely when the voltage regulating relay contacts close is required; small 120-volt neon lamps have proved quite satisfactory for this purpose.

Conclusions

1. High accuracy control for feeder voltage regulators has real economic value in distribution systems.

2. Accuracy standards are much needed by utilities and manufacturers to obtain the desired results from the application of precise control equipment.

3. The proposed ASA standards are not yet perfected, but appear to offer a workable set of rules under which the industry can now operate and obtain the experience necessary to bring them into better form.

4. The measurement of errors in class 1 and class 2 control equipment will present problems which are new to many laboratories.

Definitions

"Band width" is used in the usual sense, that is, the difference between the high and low voltage limits maintained by a regulator for a particular set of conditions.

"Voltage level" is defined as the mean value of the band limit voltages.

"Errors" represent the deviation in voltage level from that for reference conditions for various other conditions of temperature, frequency, load or regulator position. It is here proposed to define "plus errors" as those causing the regulator to maintain a voltage level higher than the reference voltage and "minus errors" as those causing the regulator to maintain a voltage level lower than the reference value. This point is discussed under Summation of Errors for "Potential Supply and Voltage Regulating Relay."

Discussion

S. Minneci (General Electric Company, Pittsfield, Mass.): Birchard has indicated the economies that are possible with the use of precise control equipment. To realize these advantages, it is essential that the voltage regulating relays, which control the operation of the feeder voltage regulators, be carefully adjusted to maintain the system voltage within the required limits. It follows therefore that adjustments on these relays must be made with voltage-measuring instruments having a high degree of accuracy.

This precaution is particularly necessary when making adjustments out in the field where the conditions may be different from those encountered in the testing laboratories. The usual portable voltmeters may not be good enough for this purpose, as the errors introduced by their use may be considerably greater than those in the voltage regulating relay being adjusted. Indicating voltmeters with large and easy-to-read scales will be a great help in keeping the errors to a minimum.

Statistical Methods as Applied to the Manufacture and Testing of Wire and Cable

M. G. WOOLFSON
MEMBER AIEE

IT is now well established that many of the processes in the manufacture of wire and cable and tests of the finished product are amenable to control or study by recognized statistical methods. The scope of these applications is indicated in part by the following.

MACHINE CONTROL CHARTS (FOR MEASURABLE CHARACTERISTICS)

1. Weights per thousand feet of concentric stranded and crushed conductors.

2. Thicknesses of walls of paper and varnished cambric insulation.

3. Dimensions of extruded insulation.

4. Eccentricity and thickness of lead sheaths.

BAR CHARTS

1. Classified abnormalities found on inspection of adequate samples or large lots of finished product (single or multiple sampling method).

2. Causes of rejection in mass production operations.

FREQUENCY DISTRIBUTIONS (HISTOGRAMS)

1. Periodic summaries of the data used for plotting machine control charts, to show material usage, and actual frequency distributions of cumulative data, and to compare performance by shifts and by machines.

2. Studies by dimensions and/or weights of the component parts of finished products.

3. Summaries of continuing records of test data such as mutual capacitance of pairs of conductors in telephone cable, ionization tests of paper-insulated, oil-impregnated lead-sheathed cable, and physical tests of bare wire, rubber, and synthetic compounds.

P-CHARTS

(Fraction defective, that is, ratio of number of defective units to the total number under consideration.)

1. Preliminary and final electrical tests of rubber-insulated wires and cables.

C-charts showing the number of defects in samples of constant size, have been used on rare occasions. In general it may be said that so far as cable manufacturing is concerned they appear to have a somewhat limited value.

Machine Control Charts

From a practical standpoint machine control charts for $\bar{X}$ (the average or arithmetic mean of a set of observed values) and R (the range, the difference between the largest observed value and the lowest observed value in the set from which $\bar{X}$ is determined) have been found most satisfactory because of the facility with which they can be introduced and used. Furthermore, they have the advantage that personnel, directly concerned with statistical control of the product, can readily understand this particular form of chart.

Three-sigma upper and lower control limits are almost always used. Occasionally, however, for special reasons, it has been found expedient to establish arbitrary limits. Such alternative limits, which include manufacturing tolerances, are invariably set by the plant engineering department. Sample groups of four are used without exception. This makes for simplicity and for uniform practice when the same or similar products are fabricated in more than one plant. $\bar{X}$ and R levels and the corresponding upper and lower control limits are recalculated at least once each month.

Depending upon circumstances, control charts are applied to each individual machine in a given department or to all of the machines comprising a group; for example, when it can be demonstrated that all of the machines in a given group have about the same characteristics they are controlled as a unit using one chart. When different products fabricated in the same department on the same machines show similar performance as to any statistic, they also are controlled by means of one chart. The factors which enter into the problem of the number of charts required for effective control are many and varied, but with the objective and the influencing factors known the determina-

Table I. Excerpt From Inspectors Work Sheet for Control of Lead Sheath

Specified Wall, Mils	Sample Number	Sheath Thickness Mils				Eccentricity, Per Cent						
		Maximum	Minimum	Total	Average	1	2	3	4	Total	Average	Range
125	258	134	117	251	125.5	6.7	4.1	3.5	4.4	18.7	4.7	3.2
125	259	132	115	247	123.5	6.9	6.7	4.1	3.5	21.2	5.3	3.4
125	260	136	121	257	128.5	5.8	6.9	6.7	4.1	23.5	5.9	2.8
125	261	132	118	250	125.0	5.6	5.8	6.9	6.7	25.0	6.3	1.3
125	262	130	124	254	127.0	2.4	5.6	5.8	6.9	20.7	5.2	4.5
125	263	133	124	257	128.5	3.5	2.4	5.6	5.8	17.3	4.3	3.4
125	264	130	122	252	126.0	3.2	3.5	2.4	5.6	14.7	3.7	3.2
125	265	134	123	257	128.5	4.3	3.2	3.5	2.4	13.4	3.4	1.9
125	266	132	123	255	127.5	3.5	4.3	3.2	3.5	14.5	3.6	1.1
							3.5	4.3	3.2			
								3.5	4.3			
									3.5			

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M. G. WOOLFSON is manager of quality control for General Cable Corporation, New York, N. Y.

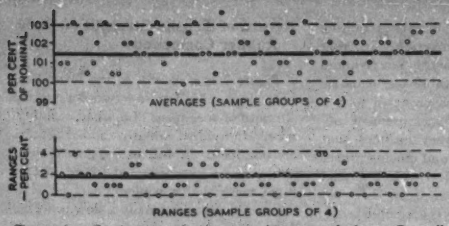


Figure 1. Continuous vulcanizing machine control chart. Over-all diameter, per cent of nominal

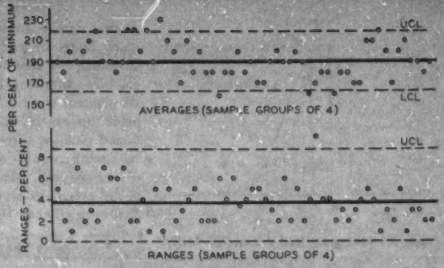


Figure 3. Continuous vulcanizing machine control chart. Tensile strength, per cent of minimum specified

tion is relatively simple. Several typical illustrations of the application of statistical methods to the control of processes in the manufacture of wire and cable and tests of the finished product are given later.

Control of Insulation Walls at Continuous Vulcanizing Machines

Figure 1 is a copy of an actual continuous vulcanizing machine control chart for $\overline{X}$ and R of over-all diameters in per cent of nominal for insulated wire (numbers 12 and 14 American wire gauge conductors with 2/64 inch of code rubber insulation). The chart covers a period of 29 successive days of operation and indicates a slight shift in $\overline{X}$ and R (averages of sets of $\overline{X}$ and R) and the corresponding upper and lower control limits from those assigned at the start of the period.

When control conditions exist, as they do substantially in this case, a specimen is taken from the finishing end of each fourth length of insulated wire for check of dimensions and for tensile strength test (in some cases, elongation). This provides a 25 per cent sampling of wire lengths. For purposes of charting, the observed data are accumulated in successive groups of four from which averages and ranges are obtained. If any plotted point falls outside of the control limits the department foreman takes necessary action to bring the operation again into control and sampling is simultaneously stepped up to 100 per cent of the wire lengths and so continued until control conditions are shown to have been again established.

Figure 2 is the corresponding machine control chart for minimum insulation thickness in per cent of the specified value. It is based on the identical specimens used in the construction of the chart, Figure 1. The same procedure as to control is used in both cases.

Tensile strength is measured using specimens from the same lengths as referred to before. The results obtained tend to be somewhat erratic because the specimens are not given sufficient time to become stable. They are, however, conservative.

Figure 3 is a control chart of tensile strength supplementing Figures 1 and 2.

Eccentricity and Average Thickness of Lead Sheaths

Two alternate methods are suggested for the control of eccentricity of lead sheaths.

1. Determining eccentricity of specimens (rings of lead) by taking 10 micrometer caliper measurements equally spaced circumferentially in accordance with the procedure outlined in the Association of Edison Illuminating Companies' specifications for Paper-Insulated, Lead-Covered Solid-Type Cable (seventh edition.) In this case eccentricity is computed using the formula

$$\text{Eccentricity per cent} = \frac{\text{Average} - \text{minimum}}{\text{Average}} \times 100$$

2. Visually examining the cut ends of the control specimens (rings of lead) to locate the thinnest and heaviest arcs and exploring these by micrometer caliper to obtain the actual minimum and maximum thickness. Eccentricity may then be computed using the formula derived from that in the preceding paragraph.

$$\text{Eccentricity per cent} = \frac{\text{Maximum} - \text{minimum}}{\text{Maximum} + \text{Minimum}} \times 100$$

Process control is a prerogative of the manufacturer and is in no manner restricted by the requirements of acceptance specifications in general use for finished products. This being so, the method of statistical control outlined in paragraph two has been chosen for the process inspection because it is less cumbersome in that it requires the recording

Figure 4. Machine control chart, vacuum-type lead press, per cent eccentricity

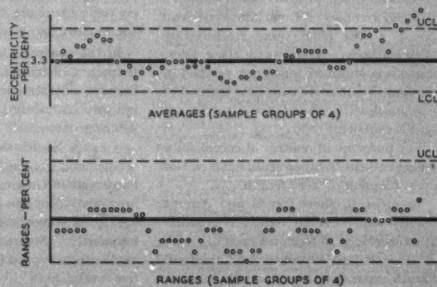
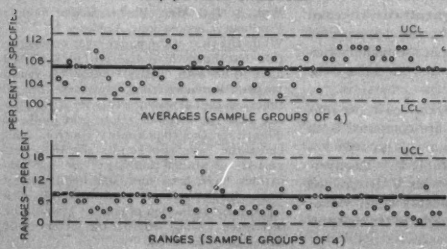


Figure 2. Continuous vulcanizing machine control chart. Minimum wall, per cent of nominal



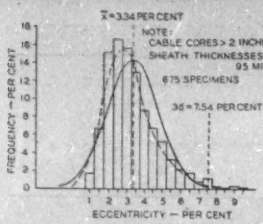


Figure 3. Histogram, lead sheath eccentricity

of only two measurements and simpler calculations. However, in all acceptance inspections to establish conformance to specifications the mandatory method stated in paragraph one is used.

If the cross section of the core and the periphery of the cable itself are circular or nearly so, as for example for 1-conductor solid or oil-filled types, eccentricity will be theoretically the same when determined by either of the two alternate methods. However, when the cross section of the cable core is not quite circular, as may be so for multiconductor solid or type *H* constructions, discrepancies may be expected. In order to determine the magnitude of these discrepancies a group of specimens (rings of lead) taken one each from 475 cables for vacuum-type lead presses at two plants were measured by both methods. Averages for these determinations obtained (one) per method of paragraph one and (two) per method of paragraph two differed by only 0.55 per cent eccentricity.

For purposes of control of eccentricity a ring of lead is taken from each end of every manhole or stock length.

In order to show trends and current quality the observed data are accumulated in groups of four, for each of which $\overline{X}$ and R are obtained and plotted. Sheath extrusion is essentially a slow process, some cable lengths requiring many hours for lead covering. In order to reduce to a minimum the time required to obtain sufficient data for one point on the control chart, each group of four is made up of the last measurement obtained plus the three previous ones, that is, moving averages of and moving ranges of four are used. Table I, which is part of an actual inspection work sheet indicates clearly how this is accomplished.

Figure 4 shows a typical control chart for eccentricity of sheath extruded from a vacuum type lead press, for cables having cores exceeding 2.0 inch diameter, and sheaths of nominal average thickness greater than 95 mils. Several types of cables have been combined. The period covered by the chart is three consecutive days of operation. Fifty-seven individual determinations are involved. The gradual trend patterns of the successive plotted points are a consequence of using moving averages ranges, adjacent points are correlated. For independent groups of four values, as in the other control charts presented here, the points are independent.

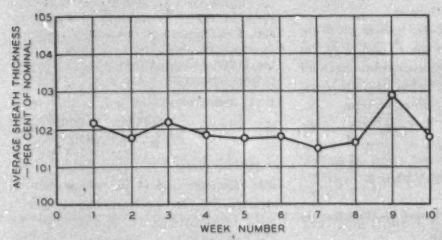


Figure 6 Control of lead sheath thickness

Figure 5 is a histogram study of eccentricity of 675 specimens of sheath representing the entire output of one vacuum-type lead press in a typical period of approximately three months. Superposed on the histogram are the theoretical normal and skewed frequency distribution curves. These are shown respectively in the illustration as solid and broken lines.

In the control of eccentricity a skewed frequency distribution is to be expected because adjustments in the manufacturing process always aim to bring the values to zero.

Average sheath thicknesses in per cent of specified nominal are computed from maximum and minimum values referred to previously (see also Table I). The average $\overline{X}$ for these averages is determined frequently. Figure 6 sets forth $\overline{X}$ values weekly for a typical period of ten consecutive weeks.

Record of Tensile Strength of Code Rubber Compound

Figure 7 is a record, in the form of a tally sheet, of laboratory tests for tensile strength of specimens of finished building wire. Individual values are added as they are obtained. It will be recognized that this is not a chronological record in the same sense that a control chart is. Its construction is necessarily a slow process. After some time, however, a pattern develops and thereafter trends away from normality are quickly recognized. Modes are expected in this form of record, reflecting changes in the insulating compounds. Unless the chart is broken down separately, for each of its components, $\overline{X}$ values cannot be properly interpreted. The rejection limit per specifications, however, is significant.

Conclusion

Several examples of the application of statistical methods to the control of the manufacturing processes and to the tests of wire and cables have been outlined in this paper. These examples have been found useful in maintaining the quality of the finished products at high levels, and in assuring conformity to specification requirements in dimensions and other controllable characteristics.

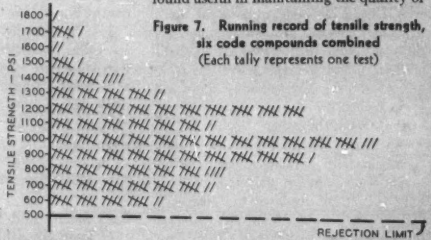


Figure 7. Running record of tensile strength, six code compounds combined
(Each tally represents one test)

Discussion

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): While the title of the paper gives no suggestion that the methods described by Woolfson have to do with quality control, his verbal presentation used that expression several times, so that it seems advisable to emphasize that the methods relate principally to quantity, rather than quality, control. This being the case, two results should be expected; assurance that dimensional specifications have been met, and reduction of waste in the use of excess material with consequent reduction of costs. I would be interested to hear whether the latter result has actually occurred.

Frequency Response Characteristics of Recording Instruments

TROY D. GRAYBEAL
MEMBER AIEE

Synopsis: Simple and direct methods are presented for ascertaining the magnitude of errors which arise from the dynamic limitations of instrument movements, and which appear in records obtained with recording instruments. Charts are given from which the a-c steady-state response may be obtained directly from the undamped natural frequency (or period) and the relative damping. These two basic constants of a particular instrument may be measured readily by the simple test methods described. Proper damping is emphasized, and certain techniques are suggested by which the frequency response may be improved beyond that obtainable with optimum damping. A simplified analysis of servo-actuated recorders suitable for application work also is included. The methods and the use of the charts presented are illustrated by means of numerical examples.

THE SUBJECT of recording instruments dates back to the early days in scientific work when it became necessary to measure quantities and to have a record which could be studied and analyzed after the measurements had been made. This subject is still of vital concern. It is essential that one know, at least to some degree, the accuracy of a record obtained from a recording instrument if the results are to be of value. The dynamic characteristics of instruments have been studied a great deal, and are quite well understood by the instrument manufacturers and by most research workers; but information dealing with this subject is not readily available in a form suitable for application work. It is the purpose of this paper to present in a simple and direct form information from which instrument errors in a given situation can be readily ascertained.

The large users of recording instruments are research laboratories and manufacturing plants, particularly refineries, chemical plants, and others which are commonly grouped under the heading of "process industries." The general trend in these industries, as well as in most others, is toward faster processes and higher speed devices to regulate them, since this in general means lower cost in production. High-speed recording is a very important aspect of this general trend. For many processes standard recording equipment is inadequate and special high-speed equipment is necessary. In others, standard equipment will suffice if properly adjusted.

For recording accurately at high speed, an instrument must have a high undamped natural frequency (low undamped natural period), and it must be properly damped. In fact, the accuracy of an instrument as related to the frequency of the signal it is recording may be expressed in terms of these two variables.

The undamped natural frequency depends upon the inertia of the instrument movement, and upon the spring constant of the restoring spring. Once an instrument is built, there is not much that can be done by the user of the instrument to increase the undamped natural frequency. The relative damping, on the other hand, is to a considerable degree under his control. Electrical instruments of the D'Arsonval type and some oscillographs obtain a considerable part of their damping effect from the electric circuit in which they are connected. Thus, the relative damping may be changed by changing the resistance of the electric circuit as seen from the terminals where the instrument is connected. In other oscillographs damping is introduced by submerging the element in oil, in which case the relative damping may be changed by the use of an oil of different viscosity. In other instruments damping is introduced by air vanes, and may be changed by adjusting the vanes. The damping of an instrument should be adjusted properly before other means to increase the frequency response are tried. Improper damping can easily account for a ten-to-one loss in frequency response.

Amplitude Response

In this paper, the amplitude response of an instrument is taken as the ratio of the recorded amplitude at a particular frequency to the recorded amplitude at zero frequency for the same amplitude of signal applied to the instrument in both cases, the basic signal being taken as a sine wave. Typical frequency response curves are illustrated in Figure 1 for different degrees of damping. The frequency is expressed as a fraction of the undamped natural frequency of the instrument, and the damping is expressed as a fraction of critical damping. Critical damping ($\beta = 1$) is defined in the usual way. It is the damping representing the transition case (as β is varied) from oscillatory to aperiodic motion following a sudden disturbance. Both ordinates and abscissa are nondimensional quantities and the curves are universally applicable to all instruments that may be considered as a linearly damped mass-spring mechanical system in which coulomb friction is assumed negligible.

The following salient features are evident from these curves:

1. For critical damping ($\beta = 1$) the instrument response is down 50 per cent at the undamped natural frequency; it is down one

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TROY D. GRAYBEAL is assistant professor of electrical engineering at the University of California, Berkeley, Calif.

The author wishes to express his gratitude to his colleagues and students for their help and suggestions with this work—in particular to A. G. Berger for aid in making calculations, to H. G. Wilson for preparing curves, to G. K. Tajima for tracing library and reference sources, and to F. E. Snodgrass for making tests and obtaining experimental data.

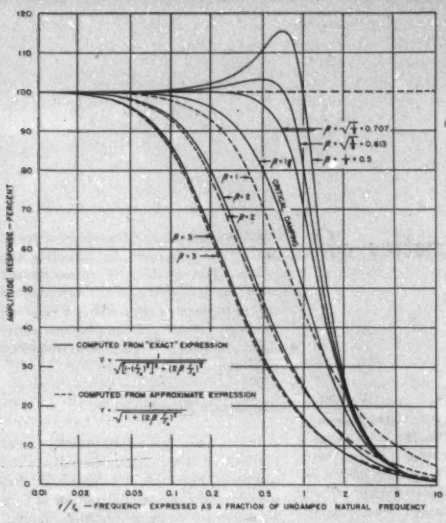


Figure 1. Nondimensional frequency response curves of an instrument

per cent at a frequency one-tenth the undamped natural frequency.

2. For a resonant peak to appear in the frequency response curve, the damping must be less than $1/\sqrt{2} = 0.707$ times critical damping.

3. For $\beta > 0.707$, the initial slope (slope at $=0$) of the frequency response curve is negative. This slope becomes zero (curve horizontal) for $\beta = 0.707$, and is positive (curve rises) for $\beta < 0.707$.

4. For accurate instrument response at the highest possible frequency, the relative damping should be near the value of 0.707.

For predicting the maximum frequency that an instrument will record with an error not greater than some specified amount, or for ascertaining the error at a given frequency, the curves of Figure 2 have been prepared. Relative damping β is taken as abscissa, and frequency expressed as a fraction of the undamped natural frequency is taken as ordinates. The curves represent loci of constant amplitude error. The range of relative damping is restricted to values of β from 0.1 to 10. For small values of β, the curves approach limiting values which may be taken as the values for $\beta = 0.1$. For values of β greater than ten, the effect of the inertia of the instrument is negligible, and the maximum frequency for a specified error may be ascertained more easily by a method to be explained later. The break in the curves near $\beta = 0.707$, arises because of the rising nature of the frequency response curves for $\beta < 0.707$. If the maximum error is specified as say ± 3 per cent (see Figure 1), the instrument may be damped to a value $\beta = 0.613$, and the resonant rise at $f/f_n = 0.5$ will not be greater than three per cent. Therefore the frequency limit for this value of β will be $f/f_n = 0.78$ where the response is down three per cent. If β be reduced below this value, the resonant rise will be greater than three per cent, and the limit of f/f_n will therefore be less than 0.50, making an abrupt break in the corresponding error locus curve at $\beta = 0.613$. The abrupt break for any curve of Figure 2 occurs for the value of β which makes the resonant rise at the peak equal to the per cent error represented by the particular error locus curve.

For slow variations, frequencies less than one cycle per second or so, it is usually more convenient to work with periods rather than frequencies. The curves of Figure 3 present the same information as those of Figure 2, except that the ordinates represent periods. The period is expressed in nondimensional form as a fraction of the undamped natural period of the instrument in order that the curves be universally applicable.

Phase Error

While the amplitude error is usually of major concern, there are times when the phase error of an instrument need be considered. The phase error arises because the motion of the instrument pointer does not exactly follow the variations in the quantity being measured. For a sinusoidally varying quantity, the instrument pointed will lag behind and reach the peak of its swing at a time later than the peak of the varying quantity being measured. This is true even though the damping is low and the amplitude response is greater than 100 per cent. This lag can be measured conveniently in degrees, a degree representing 1/360 part of one complete cycle of variation. The phase error is always a lagging angle and becomes 90 degrees at the undamped natural frequency regardless of the degree of damping. In general the phase error will be greater for frequencies below the undamped natural frequency when the damping is increased. Figure 4 gives the phase angle error for different values of β plotted against frequency.

For values of $\beta \geq 0.707$, the amplitude response is too small. The error in this case may be taken as positive. For a given relative frequency and degree of damping, the amplitude error and phase error are uniquely determined. Thus it is possible to plot curves of phase error versus amplitude error for different relative dampings. One of these variables may then be found if the other two are known. Such a set of curves is given in Figure 5. For large values of β, the curve assumes a limiting position. For values of $\beta < 0.707$, the amplitude error becomes negative and cannot be plotted as such on a log scale. If plotted as a positive quantity, the curve has three branches, and it is then necessary to know the relative frequency to know which branch applies. As the use of such multivalued curves is confusing, they have been omitted. The phase angle error for values of $\beta < 0.707$ may be obtained from the curves of Figure 4.

Illustrative Example

A recording instrument having an undamped natural period of 0.5 second is damped to a relative value $\beta = 1.5$. It is recording a signal which goes through one complete cycle in a sinusoidal manner in three seconds. How much error exists in the record obtained? Can the instrument be adjusted to give a record accurate to one per cent? If so, what is the maximum relative damping permissible, and what is the phase error under these conditions?

Solution:

$T/T_n = 3/0.5 = 6$
$f_n = 1/0.5 = 2$ cycles per second
$f = 1/3 = 0.3333$ cycle per second
$f/f_n = 0.1667$

From Figure 2 (or Figure 3) the error in the record obtained is nine per cent, the recorded value being too low.

From Figure 2 (or Figure 3), if the damping were reduced to 0.82, the error in the record obtained would be reduced to one per cent.

The phase error is 16 degrees lagging from Figure 4 (or Figure 5) for a relative damping 0.82 and an error of one per cent.

Highly-damped Instruments

When an instrument is highly damped, the component of force which arises from the inertia of the moving system is small compared with other forces and may be neglected for frequencies below the undamped natural frequency. Under these conditions the a-c steady-state response may be computed on the basis of equation 24, and the error on the basis of equation 31 of the Appendix. Equation 24 is plotted in Figure 1 as dotted lines for values of $\beta=1$ (critical damping), $\beta=2$, and $\beta=3$ for comparison with those plotted in solid lines from the exact expression which includes the inertia term.

The error computed on the basis of the approximate expression is $2\beta^3/(2\beta^2-1)$ times the error computed on the basis of the exact expression for small errors. Thus an instrument may be considered highly damped if β is greater than about two or three. For example, if the error were one per cent on the basis of the exact expression for $\beta=2$, the approximate expression would give an error of $8/7\times1$ per cent $=1.14$ per cent. If $\beta=3$, the agreement would be much better $18/17\times1$ per cent $=1.06$ per cent. This would probably be close enough for most instances in practice. The approximate expression always gives results on the conservative side.

For high damping, the maximum frequency an instrument will record with an error not greater than some specified amount may be obtained from the curves of Figure 2 or Figure 3 if the relative damping and the undamped natural frequency are known and if $\beta<10$. If $\beta>10$, or if β and f_n are not known independently, the curves of Figure 6 or Figure 7 may be used. In these curves ΔT is a new variable being defined as the time required for the instrument to reach 50 per cent of its final steady-state deflection after a sudden step-function disturbance. ΔT may be computed from β and f_m (or T_n) if these are known.

$$\Delta T=0.2205\beta/f_n$$

or

$$\Delta T=0.2205\beta T_n \qquad (40)^*$$

*(See numbered equations in Appendix)

Illustrative Example

An instrument having an undamped natural period of 0.5 second is damped to a relative value $\beta=4.5$. What is the minimum period of an a-c signal that can be recorded with an error not exceeding five per cent?—one per cent?

Solution:

$$\Delta T=0.2205\ (4.5)\ 0.5=0.496 \text{ second}=496 \text{ milliseconds}$$

From Figure 7: Minimum period for five per cent error $=13.5$ seconds. Minimum period for one per cent error $=31$ seconds.

These values may be checked from the curves of Figure 3: Minimum period for five per cent error $=0.5\ (27.0)=13.5$ seconds; for one per cent error $=0.5\ (62)=31$ seconds.

Determination of Instrument Constants by Test

In order to use the curves of Figures 2 to 7, it is necessary to know the constants

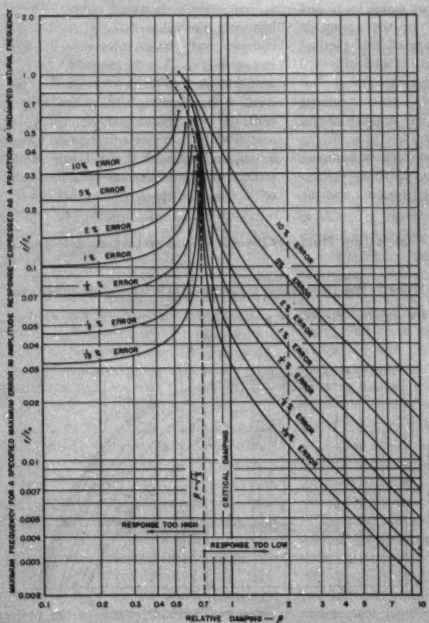


Figure 2. Amplitude error curves of an instrument in terms of frequency

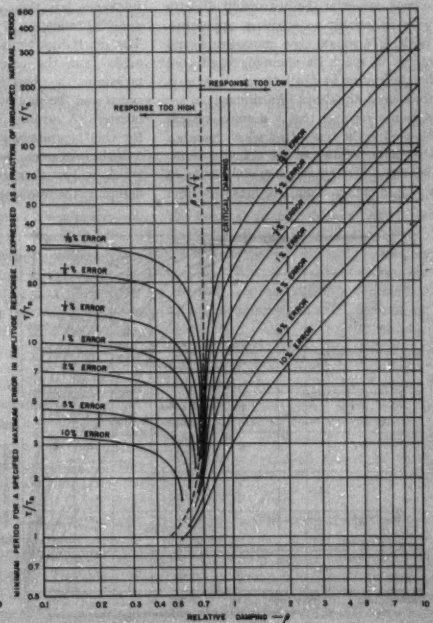


Figure 3. Amplitude error curves of an instrument in terms of period

of the instrument β and f_n, or ΔT if the relative damping is known to be at least two or three times critical damping. The constants may be found from either a-c steady-state response tests, or from transient tests. The particular method which is most convenient will depend upon the auxiliary attachments built into the particular instrument being tested, as well as upon the type of testing equipment available. The results obtained by some methods are less subject to errors arising from imperfections of the instrument, notably friction introduced by the pen moving on the chart, and would be preferred where a choice of methods is available. Two or more methods might be used and the most probable values of β and f_n selected on the basis of judgment if the constants need be determined with a high degree of accuracy. In most applications, however, this is not necessary.

If equipment is available for a-c steady-state response tests, the frequency response may be obtained directly from such tests. The test equipment required, however, is much more elaborate than that required for transient tests, and for this reason the latter probably would be used more often. Many recorders are provided with chart drives which may be driven fast enough to obtain transient response curves of the instrument directly with essentially no auxiliary apparatus. With D'Arsonval-type movements and others in which the damping is affected by the system in which the instrument is to be used, the direct measurement of a-c response requires that a dummy system be used in the test set-up which matches the characteristics of the actual system; otherwise corrections or modifications in the data obtained must be made. These modifications can be made most effectively by computing f_n and β_o directly from test data. β_o is the relative damping inherent in the instrument itself. These values then would be used in the same manner as those obtained from transient data for predicting the performance of the instrument in the system in which it would be used. Some transient methods have the advantage that the transient response tests may be made with the instrument connected into the system with which it is used, and the value of relative damping β obtained includes the component reflected into the instrument by the system.

A-C Steady-State Methods

The undamped natural frequency f_n can be found by applying an a-c signal to the instrument and adjusting the frequency of the signal until the applied signal and the motion of the instrument pointer are 90 degrees out of phase. The frequency of the applied signal is then equal to f_n. This method of finding f_n is independent of the relative damping. The relative damping then may be found by measuring the per unit response (actual response divided by signal amplitude, both expressed in the same units) at some frequency less than or equal to f_n and applying equation 7 of the appendix. The chief disadvantage of this method arises from the practical difficulties of measuring the phase between the applied signal and the motion of the instrument pointer. A stroboscope is often useful for this measurement.

If the instrument has inherent damping which is less than 0.707 times critical damping, the resonant frequency and the per unit response at the resonant frequency may be obtained from a frequency response curve. The undamped natural frequency then may be found from equation 10 and the relative damping from equation 11 of the Appendix. This method gives good results for $\beta<0.6$ where the resonant peak is sufficiently sharp for accurate determination of the resonant frequency f_r. One major limitation of this method is that many instruments have inherent damping greater than 0.707 and the method is not applicable.

Transient Methods

Method 1. If the damping is less than critical damping, β may be found from the ratio of successive positive and negative swings in accordance with equation 21 which is plotted in Figure 8. The undamped natural frequency then may be obtained from the frequency of the damped oscillation by means of equation 9. This equation also is plotted in Figure 8. This method gives good results for β less than about 0.8, but is subject to some error from pen friction. The effect of pen friction is to clip off the peaks of the swing so that the numerical value of X_o/X_1 obtained is somewhat too large. The effect is not serious, however, since a large change in the value of X_o/X_1 results in a comparatively small change in β. For instance, if $X_o/X_1=20$, $\beta=0.69$. If X_o/X_1 is increased to 30, an increase of 50 per cent, β increases to 0.77, about 12 per cent. If β is greater than about 0.8, the amplitude of successive swings decreases by such a large ratio that it is impossible to determine even the order of magnitude of X_1 from the chart record, particularly

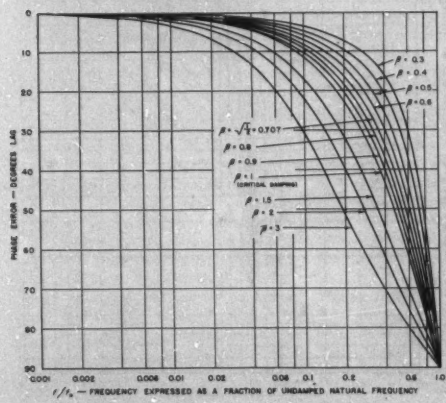
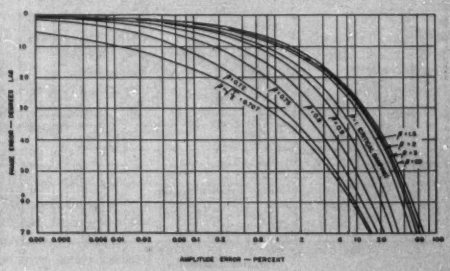


Figure 4 (left). Phase error curves of an instrument in terms of frequency

Figure 5. Phase error curves of an instrument in terms of amplitude error

For relative damping less than 0.707, the curves of Figure 4 may be used



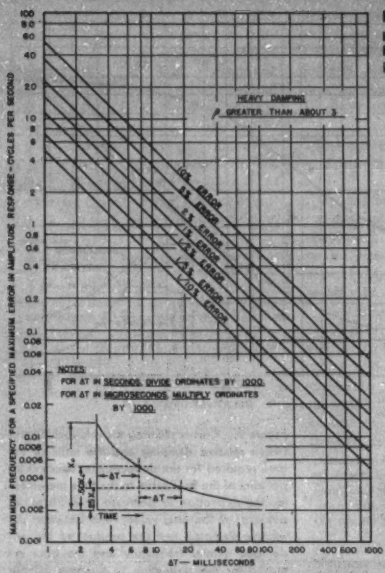


Figure 6 (left). Amplitude error curves for highly-damped instruments in terms of frequency

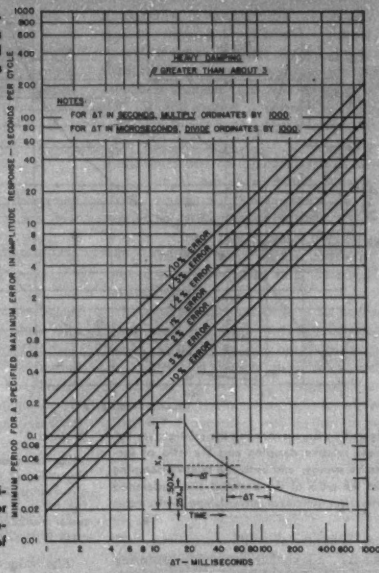


Figure 7. Amplitude error curves for highly-damped instruments in terms of period

if there is any appreciable pen friction; so that the method is not applicable for values of relative damping near the critical value. In some galvanometers, the damping inherent in the instrument is very low and X_o and X_1 are very nearly equal, thus making it difficult to determine β accurately. This difficulty may be overcome by computing the ratio of decay over several swings of the instrument. Thus if n swings are considered (each positive and negative swing count), the "n"th root of this ratio would be computed and this value used to enter the curves of Figure 8.

Method 2. As explained in the Appendix, the actual shape of the transient response curve depends only upon the relative damping, while the time required for a certain change in deflection to occur depends primarily upon the undamped natural frequency. Making use of this fact, the curves of Figure 9 have been prepared. T_1 is the time required for the deflection to reach 50 per cent of the final steady-state value, while ΔT is the time increment for the deflection to decrease from 50 per cent to 25 per cent of its final steady-state value. By computing the value of $\Delta T/T_1$ from the transient response curve, the relative damping can be found from the curve $\Delta T/T_1$. The value of f_nT_1 can be found from the other curve. From this value and the actual time value of T_1 from the transient response curve, the undamped natural frequency f_n can be computed. This method gives fairly good results for $0.8 < \beta < 2$ if pen friction is not too great. This method is more subject to pen friction error than *Method 1*. For this reason, it is often desirable to obtain the relative damping β_o for the instrument on open circuit, if a D'Arsonval type, and to correct this value to take into account the damping effect introduced by the circuit in which the instrument is connected. A method of doing this is described later.

An important source of error is the limit of accuracy of making measurements on the chart, and the fact that the circular cross rulings on the chart usually do not correspond accurately with the arc described by the pen of the instrument. This latter effect can be checked by allowing the pen to swing over the chart while the chart is stationary, and allowance may be made for it if appreciable.

An idea of the degree of accuracy that may be obtained practically by the two methods outlined is illustrated by the curves of Figures 16 and 17. The 5-milliampere instrument is a "standard" commercial instrument, while the 3-milliampere instrument is classified as a high-torque instrument. The effect of pen friction is much greater for the 5-milliampere instrument. This fact accounts for the greater discrepancy between points obtained from transient records and the computed curve. For the 3-milliampere instrument, the discrepancy is due principally to the limit of accuracy of making measurements on the chart. The effect of pen friction on the a-c steady-state response is illustrated by the curve for 10,000 ohms in Figure 14. The instrument was damped slightly below 0.707, so that a slight rise would be expected in the frequency response curve. The response actually drops about 1.5 per cent at a frequency of 0.2 cycle per second principally because of pen friction, and then rises to 99 per cent response at 0.6 cycle per second before dropping off as the undamped natural frequency is approached. This pen friction effect is not noticeable in the curves of Figure 13 which were obtained on the high-torque instrument. In obtaining these response curves, a signal corresponding to 80 per cent of the full scale amplitude was used. For smaller amplitudes, the percentage effect of pen friction would be much greater. This effect also varies with chart speed.

Method 3. For values of relative damping greater than about two, the ratio $\Delta T/T_1$ is very nearly unity, and the curves of Figure 6 or 7 may be used to obtain the a-c steady-state frequency error directly. For values of β near two where

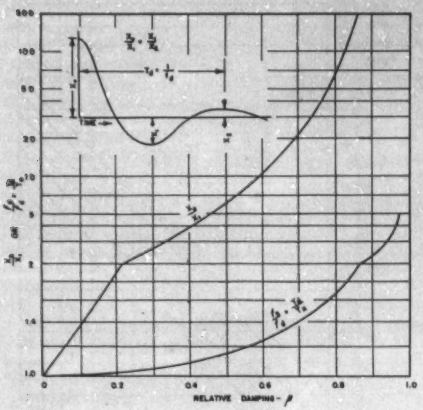


Figure 8. Curves showing the relationship between relative damping and the ratio of successive swings; and between relative damping and the ratio of the damped and undamped natural frequencies of underdamped instruments

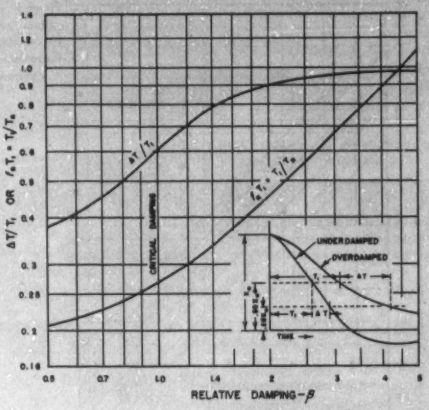


Figure 9. Curves showing the relationship between relative damping and the ratio of the time required for the deflection to become 50 per cent of the final value to the time increment for the deflection to decrease from 50 to 25 per cent of the final value; and between the relative damping and the product of the undamped natural frequency and the time required for the deflection to become 50 per cent of the final value

there is appreciable difference between T_1 and ΔT, ΔT should be used for entering the curves of Figures 6 and 7.

Illustrative Example

An instrument having the characteristics illustrated in Figure 11 operates in a circuit having a driving point resistance of 10,000 ohms. What is the maximum frequency the instrument will record with an error not greater than 2 per cent? What will be the maximum frequency if the circuit resistance is 4,000 ohm?—zero ohms?

Solution:

Method 1.

For the case with 10,000 ohms resistance $X_o = 1.00$, $X_1 = 0.060$, and $f_d = 1.97$ cycles per second by measurements made directly on the chart record.

$X_o/X_1 = 16.7$. From curves of Figure 8, $\beta = 0.67$, $f_n/f_d = 1.35$.

From these values, $f_n = 1.35\ (1.97) = 2.66$ cycles per second.

From curves of Figure 2, $f/f_n = 0.60$.

The maximum frequency for which the error will not exceed two per cent is therefore $0.60\ (2.66) = 1.58$ cycles per second.

Method 2.

	For 10,000 Ohms	For 4,000 Ohms	For Zero Ohms
Measurements taken directly from chart records	$T_1 = 0.085$ second	0.105 second	0.267 second
	$\Delta T = 0.038$ second	0.064 second	0.258 second
Computing the ratio	$\Delta T/T_1 = 0.448$	0.610	0.957
From Figure 9	$\beta = 0.69$	1.02	3.0
	$f_nT_1 = 0.225$	0.270	0.67
Dividing f_nT_1 by T_1	$f_n = 2.65$ cycles per second	2.58 cycles per second	2.50 cycles per second
From curves of Figure 2	$f/f_n = 0.5$	0.17	0.035
Maximum frequency for 2 per cent maximum error is	1.32 cycles per second	0.44 cycles per second	0.0875 cycle per second

Method 3.

For the case with zero external resistance, the value of maximum frequency could be obtained more readily from Figure 6. Using the value of $\Delta T = 0.258$ seconds, the maximum frequency for two per cent maximum error is 0.087 cycles per second.

The values computed in this example may be compared with the frequency response curves shown in Figure 13 which were obtained on the same instrument by actual test.

Component of Relative Damping Reflected From the Electrical Side—D'Arsonval Movement

It is shown in the Appendix that for a D'Arsonval-type instrument, the relative damping β consists of two components, one which represents the damping inherent in the movement when the coil circuit is open (β_o) and another component which is inversely proportional to the resistance of the coil circuit (β_1).

$$\beta = \beta_o + \beta_1 \tag{56}$$

where

$$\beta_1 = F/R_T \tag{59}$$

$$F = 9.8\pi l K f_n \times 10^{-8}/(I_p)^2 \tag{60}$$

Equation 59 is plotted in Figure 10 with β_1 as a parameter. These curves may be used for computing the component of damping which is reflected into the mechanical system of the instrument from the electrical circuit in which the instrument is connected. This component of relative damping when added to the component of relative damping inherent in the instrument movement gives the relative damping of the instrument. To compute the factor F, one needs to know the undamped natural frequency of the instrument, the coil current for full scale deflection, the length of arc on the chart which represents full scale, and the force that must be exerted at the writing point of the pointer to cause full scale deflection. If the instrument is shunted, the shunt must be considered as part of the network into which the instrument is connected. That is, the current I_p is the current for full scale deflection which actually flows in the moving coil of the instrument. The parameter R_T is the actual coil resistance plus the resistance of the network and the resistance of the shunt combined in parallel.

Illustrative Example

An instrument is internally damped (value of damping when coil circuit is

open) to a relative value of 0.37. The undamped natural frequency is 2.60 cycles per second, the resistance of the moving coil is 1,324 ohms, the current range is three milliamperes, the length of the chart scale is 12 centimeters, and the force that must be applied at the end of the pointer to produce full scale deflection is 3.26 grams. The instrument is not shunted. What must be the resistance of the external circuit to give critical damping?—70 per cent critical damping?

Solution:

$$F = \frac{9.8\pi(12)\ 3.26\ (2.60)\ 10^{-4}}{(0.003)^2} = 3{,}480$$

For critical damping, $\beta_1 = 1 - 0.37 = 0.63$.

From Figure 10, $R_P = 5{,}520$ ohms and the external circuit resistance $= 4{,}200$ ohms.

For 70 per cent critical damping, $\beta_1 = 0.70 - 0.37 = 0.33$.

From Figure 10, $R_T = 10{,}530$ ohms and the external circuit resistance $= 9{,}210$ ohms.

Application of Curves to Systems Other Than Recording Instruments

The curves presented in Figures 2 to 9 are applicable to any type of mechanical system which may be represented by a second order differential equation. Such a list would include galvanometers, oscillograph elements, liquid level gauges, pressure gauges, as well as the simpler types of servomechanisms.

Methods of Increasing Frequency Response

In applications where the frequency response of an instrument is not fast enough to give a sufficiently accurate record, the response may be improved in several ways, but usually at a sacrifice in sensitivity.

Any practical solution usually represents a compromise between these two conflicting factors. As a rule a D'Arsonval-type movement should not be shunted except by comparatively high-resistance networks if the maximum frequency response capabilities of the instrument are to be realized.

In making adjustments to increase frequency response, the first consideration should be given to damping. Unless the damping is adjusted closely to the optimum value, a considerable loss in frequency response will result. For instance, if an instrument having an undamped natural frequency of one cycle per second is damped to a relative value of 0.67, it will record a frequency of 0.5 cycle per second with an error not greater than one per cent. But if the damping is increased to critical damping, the maximum frequency that can be recorded with an error not greater than one per cent is only 0.1 cycle per second. Thus, if an instrument is damped to the critical value, the frequency response can be improved by a five-to-one ratio by adjustment of damping.

If the frequency response of an instrument must be improved beyond that which can be achieved by optimum damping, special techniques must be resorted to. Some improvement may be achieved by replacing the restoring springs of the instrument movement with springs having a larger spring constant. This, of course, will reduce the sensitivity of the instrument. If this loss of sensitivity is not objectionable, still the improvement that can be achieved in this way probably would never be greater than about a two-to-one ratio. For a two-to-one improvement, the moving coil of the instrument would have to carry four times the original current for the same deflection

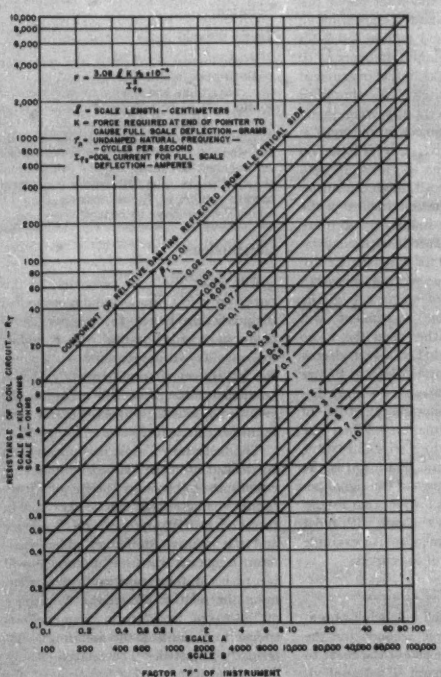


Figure 10 (left). Curves showing the relationship between the component of relative damping reflected from the electrical circuit of a D'Arsonval-type movement, the resistance of the coil circuit of the instrument, and the "Conversion" factor F

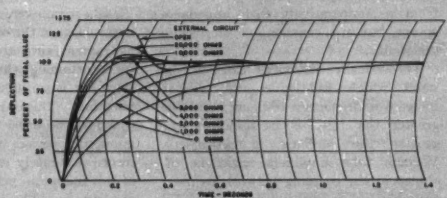


Figure 11. Transient response curves of a 3-milliampere recording milliammeter

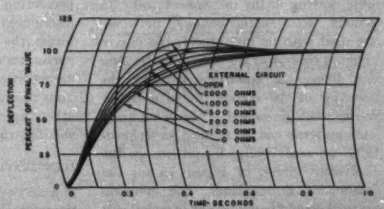


Figure 12. Transient response curves of a 5-milliampere recording milliammeter

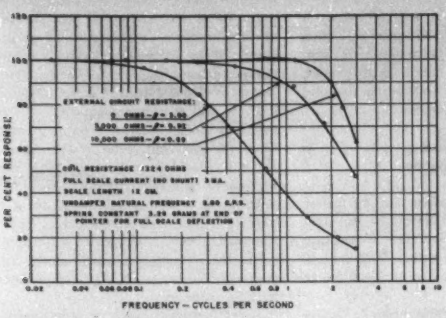


Figure 13. Frequency response curves of a 3-milliampere recording milliammeter obtained by actual test

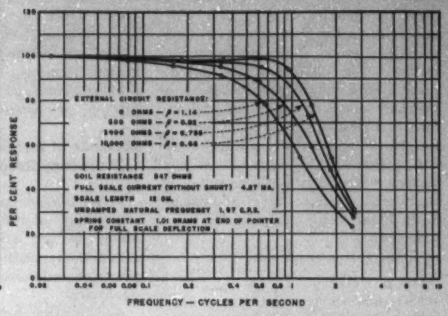


Figure 14. Frequency response curves of a 5-milliampere recording milliammeter obtained by actual test

Unless the coil originally were designed very conservatively, even a four-to-one increase in the coil current might be sufficient to damage the coil from overheating.

The frequency response of D'Arsonval-type movements may be increased by the use of special coupling networks connected between the circuit where the measurement is to be made and the coil circuit of the instrument. The simplest network of this type is an inductive shunt connected across the instrument coil terminals. By the proper choice of the resistance and inductance of this shunt, the frequency response may be increased considerably if the instrument is supplied from a comparatively high impedance source, but at a considerable sacrifice in sensitivity.

Theoretically, networks can be devised which will give any desired increase in frequency response; but the greater the increase in response, the greater the sacrifice in sensitivity. Vacuum tube amplifiers might be included in the coupling network to overcome the loss in sensitivity, but the upper limit of frequency response attainable is usually limited by the maximum permissible heating in the instrument coil. In general, when special coupling networks are used with an instrument, the curves given in this paper do not apply, because the differential equations defining the behavior of the system are of a higher degree than the second. The behavior of such systems can be studied experimentally, or analytically, by setting up the equivalent circuit of Figure 15 for the instrument in question. The coupling network would be connected in series with $E(t)$, and the current i_L would be studied as a function of frequency.

Servo-Actuated Recorders

There are available at the present time a number of different types of servo-actuated recorders mostly in the form of self-balancing potentiometers. These instruments require a relatively small signal, of the order of one per cent to five per cent of full scale, to produce maximum velocity of the recording pen. The time T_{fs} required for the pen to move from one end of the chart to the other under this condition is a standard specification of such instruments, and can be used for estimating the maximum frequency, or minimum period, which the instrument will record accurately. A simplified analysis which neglects any error introduced by the servomechanism itself is given in the Appendix. Curves based on this analysis are given in Figure 18 with T_{fs} as a parameter. Although the curves neglect lags in the servomechanism of the instrument, they are sufficiently accurate for ascertaining whether a particular instrument is suitable for a particular application. This is true because one wishes to know the order of magnitude of the minimum period the instrument will record accurately rather than the value of this minimum period to a high degree of accuracy. The curves given in Figure 18 are based on sine waves simply because this is one convenient method of presenting the information. When the instrument is recording irregular wave forms, the fundamental limitation is the maximum rate of change of the signal being recorded, and not the fact that it contains harmonics as is the case with most other types of instruments. An example will illustrate the use of the curves.

Illustrative Example

A servo-actuated self-balancing potentiometer requires three seconds for the pen to move from one end of the chart to the other with maximum signal applied. The recorder is to be used to record a signal which has a period of five seconds and which varies in a sinusoidal manner. What is the maximum peak-to-peak amplitude on the chart for which the instrument will record this signal accurately?

Solution:

$T_{fs} = 3$ seconds

From Figure 18, scale B, the maximum peak-to-peak amplitude permissible is 53 per cent. The sensitivity of the recorder would have to be adjusted by means of an external voltage divider or similar method so that the maximum signal available would not drive the pen over more than 53 per cent of the chart range. If the full width of the chart were to be utilized, an instrument having a value of T_f, not greater than 1.6 seconds would have to be used.

Conclusions

The chart methods presented in this paper provide a simple and direct method of predicting either the magnitude of errors in records obtained with recording instruments when recording a-c quantities or the maximum frequency that a particular instrument will record if the error is not to exceed some specified maximum per cent error.

The behavior of most instruments can be defined by a second order differential equation. In this case the frequency response characteristics can be expressed in terms of the undamped natural frequency and the relative damping. The maximum frequency that an instrument will record accurately is critically dependent upon the relative damping. A sacrifice of ten-to-one or more in frequency response may result if the damping is improperly adjusted. The relative damping should be somewhat less than 0.7 for

maximum frequency response, the exact value being determined by the maximum error that can be permitted in the record obtained.

The undamped natural frequency and relative damping may be measured by a-c steady-state or transient test methods. Transient tests usually require less complicated testing equipment, and for this reason methods based upon transient tests usually are preferable. The values of undamped natural frequency and relative damping obtained by test methods are subject to some error from imperfections of the instrument, notably friction between the pen and chart, as well as from the limit of accuracy of making measurements on chart records. The former is not serious in a well designed properly adjusted instrument, and the latter may be minimized by the choice of a suitable chart speed.

For instruments in which the relative damping is affected by the system in which the instrument is connected when making measurements, the relative damping usually may be obtained more accurately by measuring the relative damping inherent in the instrument itself, and adding to this value the component of relative damping reflected into the movement by the system with which the instrument is used. For D'Arsonval-type movements, the component of relative damping reflected from the electrical circuit of the instrument is inversely proportional to the coil circuit resistance. The proportionality of "conversion" factor may be computed from the undamped natural frequency, the coil current for full scale deflection, the length of the arc on the chart which represents full scale, and the force that must be exerted at the writing point of the pen to cause full scale deflection.

If the relative damping is greater than about two or three, the component of force arising from the inertia of the instrument movement may be neglected for frequencies less than the undamped natural frequency. The frequency response characteristics then may be expressed in terms of ΔT, the time required for the pointer deflection to assume 50 per cent of its final steady-state deflection following a step-function disturbance. The parameter ΔT may be computed from the relative damping and the undamped natural frequency, or it may be obtained directly from a transient test.

It is possible to increase the frequency response of an instrument beyond that obtainable with optimum damping, either by making modifications in the instrument movement to increase the undamped natural frequency, or, for D'Arsonval-type movements, by the use of coupling networks between the coil circuit of the instrument and the network where the measurement is to be made. Both these methods represent a sacrifice in sensitivity. The upper limit that can be achieved practically depends upon the maximum permissible coil current that will not damage the coil of the instrument from overheating.

For those types of servo-actuated recorders where a signal of the order of one per cent to five per cent of full scale will give maximum velocity of the recording pen, the upper limit of frequency for which the recorder will give an accurate record may be expressed in terms of the time required for the pen to move from one end of the chart to the other at maximum velocity. The results obtained from this simplified analysis are sufficiently accurate for application work.

Appendix

A-C Steady-State Solution

The differential equation of motion of the instrument movement may be written

$$M\frac{d^2X}{dt^2}+C\frac{dX}{dt}+KX=f(t) \tag{1}$$

where M, C, K, and X may be expressed in any consistent system of units, either translational or rotational. Dividing by M and writing p for d/dt:

$$\left(p^2+\frac{C}{M}p+\frac{K}{M}\right)X=\frac{1}{M}f(t) \tag{2}$$

which reduces to:

$$(p^2+2\omega_n\beta p+\omega_n^2)\,X=\frac{1}{M}f(t) \tag{3}$$

by substitution of

$$\omega_n=\sqrt{K/M}=\text{Undamped natural angular velocity} \tag{4}$$

$$\beta=C/C_c=\frac{C}{2\sqrt{MK}}=\frac{\text{Damping coefficient}}{\text{Critical damping coefficient}} \tag{5}$$

The steady-state solution in vector form for a sinusoidal driving force equal to 100 per cent amplitude is[8]:

$$Y=\frac{1}{\sqrt{(1-\omega^2/\omega_n^2)^2+(2\beta\omega/\omega_n)^2}}\Bigg|\underline{-\tan^{-1}\frac{2\beta\omega/\omega_n}{1-(\omega/\omega_n)^2}} \tag{6}$$

$$Y=\frac{1}{\sqrt{(1-f^2/f_n^2)^2+(2\beta f/f_n)^2}}\Bigg|\underline{-\tan^{-1}\frac{2\beta f/f_n}{1-(f/f_n)^2}} \tag{7}$$

(amplitude) (phase)

in which the undamped natural frequency

$$f_n=\frac{1}{2\pi}\sqrt{\frac{K}{M}} \tag{8}$$

the damped natural frequency

$$f_d=f_n\sqrt{1-\beta^2} \tag{9}$$

the resonant frequency

$$f_r=f_n\sqrt{1-2\beta^2} \tag{10}$$

The steady-state response at the resonant frequency

$$=\frac{1}{2\beta\sqrt{1-\beta^2}}\quad\beta<1/\sqrt{2}=0.707 \tag{11}$$

The amplitude as given by equation 7 is plotted in Figure 1 (solid lines) for different values of β. The phase is plotted in Figure 4. Figure 5 is a cross plot of $(1-|Y|)$ and phase (Y) for $\beta\geq0.707$.

Transient Solution

The response of the instrument movement to a sudden step function disturbance, no driving force but with an initial displacement, is defined by the differential equation:

$$(p^2+2\omega_n\beta p+\omega_n^2)X=0 \tag{12}$$

and boundary conditions:

at $t=0$, $X=100$ per cent and $dX/dt=0$

Solutions:

Underdamped case, $\beta<1$

$$X=\frac{1}{\sqrt{1-\beta^2}}\,\epsilon^{-\beta\omega_n t}\sin\left[\sqrt{1-\beta^2}\,\omega_n t+\phi\right] \tag{13}$$

where

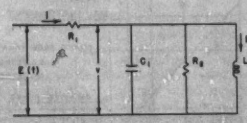
$$\tan\phi=\frac{\sqrt{1-\beta^2}}{\beta} \tag{14}$$

Critically-damped case, $\beta=1$

$$X=(1+\omega_n t)\,\epsilon^{-\omega_n t} \tag{15}$$

Overdamped case, $\beta>1$

$$X=\frac{\beta+\sqrt{\beta^2-1}}{2\sqrt{\beta^2-1}}\,\epsilon^{-(\beta-\sqrt{\beta^2-1})\omega_n t}-\frac{\beta-\sqrt{\beta^2-1}}{2\sqrt{\beta^2-1}}\,\epsilon^{-(\beta+\sqrt{\beta^2-1})\omega_n t} \tag{16}$$



$R_1 = R_2$ OHMS

$C_1 = \frac{M}{U_E U_M}$ FARADS

$R_2 = \frac{U_E U_M}{C}$ OHMS

$L_1 = \frac{U_E U_M}{K}$ HENRYS

I = COIL CURRENT-AMPS.

E(t) = VOLTAGE ACROSS COIL TERMINALS-VOLTS

X = POINTER DEFLECTION-PER UNIT OF FULL SCALE

$X = \frac{1}{U_E}\int V\,dt$

BUT SINCE: $I_L = \frac{1}{L_1}\int V\,dt$

$I_L = \frac{K}{U_E}X = \frac{U_M}{L_1}X$

Figure 15. Equivalent circuit which represents a D'Arsonval-type instrument

Relationship Between Successive Positive and Negative Peaks for Underdamped Case

$$D = \frac{\text{any maximum}}{\text{the next following minimum}}$$

$$= -\epsilon^{\frac{\pi\beta}{\sqrt{1-\beta^2}}} \qquad (19)$$

This ratio is exact and does not depend upon ϕ. Disregarding the negative sign:

$$\ln D = \frac{\pi\beta}{\sqrt{1-\beta^2}} \qquad (20)$$

which solved for β gives:

$$\beta = \frac{\ln^2 D}{\sqrt{\pi^2+\ln^2 D}} \qquad (21)$$

This equation is plotted in Figure 8, in which $D = X_2/X_1$.

Relationship Between Damped Natural Frequency and Undamped Natural Frequency

From equation 9

$$\frac{f_d}{f_n} = \sqrt{1-\beta^2} \qquad (22)$$

This equation is plotted in Figure 8.

Auxiliary Curves for the Determination of β

In equation 13, $\omega_n t$ may be replaced by another variable say Z:

$$X = \frac{1}{\sqrt{1-\beta^2}}\epsilon^{-\beta Z}\sin\left[Z\sqrt{1-\beta^2}+\phi\right] \qquad (23)$$

in which case ω_n and t both disappear. This means that the shape of the curve is independent of ω_n, since if ω_n is doubled, the time scale may be doubled (each scale unit representing half the time) and the two curves will be identical. The shape of the curve is determined completely by β, and its time calibration by ω_n.

One may compute β by any convenient method which depends upon the shape of the curve, and not upon its time calibration. The same statements also apply for the critical and overdamped solutions, equations 15 and 16. To cover the range of values of β near the critical value where the log decrement, equation 21, does not yield accurate results on the underdamped side and where the approximate method to be described later does not yield reliable results on the overdamped side, the ratio of the time required for the displacement to reach 50 per cent of its final value and the time increment required for the displacement to decrease from 50 to 25 per cent of its final value, $T_1/\Delta T$, is used as a parameter for finding β. Values of T_1 and ΔT were obtained by solving equations 13, 15, and 16 by trial for different values of β from 0.5 to 5. The resultant curve of $\Delta T/T_1$ is plotted in Figure 9, together with a curve of f_nT_1, both as functions of β. From a transient response curve of an instrument, the value of β for the instrument can be found from the curve $\Delta T/T_1$; and the value of f_nT_1 obtained from the other curve enables one to compute f_n from the time calibration of the transient record.

Approximations for Heavy Damping—Steady-State Frequency Response Curves

For large values of β, equation 7 may be written:

$$Y \approx \frac{1}{\sqrt{1+4\beta^2(f/f_n)^2}} \qquad (24)$$

This equation is plotted in Figure 1 (dotted lines) for $\beta = 1, 2, 3$ for comparison with the corresponding curves plotted from equation 7.

For small errors, the error obtained on the basis of equation 24 may be compared with that obtained on the basis of equation 7 by making use of series expansions. From equation 7

$$Y_{\text{exact}} = 1-(2\beta^2-1)(f/f_n)^2 + (1-6\beta^2+6\beta^4)(f/f_n)^4 \ldots \qquad (25)$$

$$\xi_{\text{exact}} = \text{error} = (2\beta^2-1)(f/f_n)^2 \text{ neglecting all but the first two terms} \qquad (26)$$

From equation 24:

$$Y_{\text{approx.}} = 1-2\beta^2(f/f_n)^2+6\beta^4(f/f_n)^4 \ldots \qquad (27)$$

$$\xi_{\text{approx}} = \text{error} = 2\beta^2(f/f_n)^2 \text{ neglecting all but the first two terms} \qquad (26a)$$

$$\text{Ratio } \frac{\xi_{\text{approx.}}}{\xi_{\text{exact}}} = \frac{2\beta^2}{2\beta^2-1} \qquad (28)$$

The value of ΔT from equation 39 may be substituted into equation 24 to obtain Y directly in terms of ΔT:

$$Y = \frac{1}{\sqrt{1+(2\pi)^2(1.443\,\Delta T)^2 f^2}} = \frac{1}{\sqrt{1+(9.07 f\Delta T)^2}} \qquad (29)$$

For a maximum error of ξ per cent

$$\frac{100-\xi}{100} = \frac{1}{\sqrt{1+(9.07 f\Delta T)^2}} \qquad (30)$$

which solved for ξ:

$$\xi = 100\left(1-\frac{1}{\sqrt{1+(9.07 f\Delta T)^2}}\right) \text{per cent} \qquad (31)$$

$$= \frac{100(9.07 f\Delta T)^2}{1+(9.07\,\Delta T)^2+\sqrt{1+(9.07 f\Delta T)^2}} \qquad (32)$$

$$\approx (64.2 f\Delta T)^2 \text{ per cent for small errors where } (9.07 f\Delta T)^2 << 1 \qquad (33)$$

Solving equation 30 for f:

$$f = \frac{\sqrt{\epsilon(200-\xi)}}{9.07(100-\xi)\,\Delta T} \qquad (34)$$

This equation is plotted in Figure 6 with ξ as a parameter. The reciprocal, which is the period, is plotted in Figure 7. The range of ΔT plotted is from 0.001 second to

Figure 16. Comparison of the values of the component of relative damping reflected from the electrical side of a 3-milliampere recording milliammeter as obtained by calculation with those obtained from actual test by transient response methods

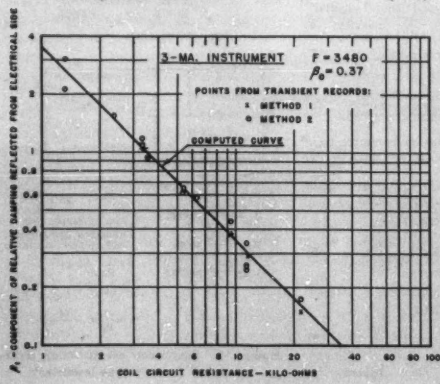
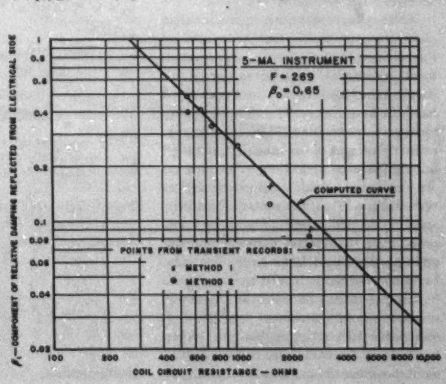


Figure 17. Comparison of the values of the component of relative damping reflected from the electrical side of a 5-milliampere recording milliammeter as obtained by calculation with those obtained from actual test by transient response methods



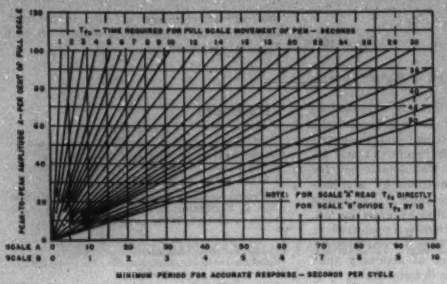


Figure 18. Curves showing the relationship between the minimum period and the peak-to-peak amplitude of a recorded signal for accurate response of a servo-actuated instrument in terms of the time required for full scale movement of the recording pen

1 second, which covers the range of most commercial instruments. The range of ΔT may be extended readily, however, as indicated by notes on the curves for values of ΔT which may fall outside the range plotted.

Approximations for Heavy Damping—Transient Response Curves

For large values of β, equation 16 may be written:

$$X = \epsilon^{-\frac{\omega_n t}{2\beta}} = \epsilon^{-\frac{t}{1.443\Delta T}} \tag{35}$$

since

$$\beta - \sqrt{\beta^2 - 1} = 1/2\beta + 1/8\beta^3 + 1/16\beta^4 + \ldots \tag{36}$$

This procedure is equivalent to neglecting the p^2 term in equation 12 and solving for X.

$2\beta/\omega_n$ is the time constant of the resultant damped-spring mechanical system. This may be replaced by the variable ΔT:

$$0.5 = \epsilon^{-(\omega_n/2\beta)\Delta T} \text{ by definition} \tag{37}$$

from which:

$$\Delta T = \frac{2\beta}{\omega_n} \ln 2 = \frac{1}{1.443}\left(\frac{2\beta}{\omega_n}\right) \tag{38}$$

$$\Delta T = \frac{\text{Time constant}}{1.443} \tag{39}$$

$$\Delta T = 0.2205\ \beta/f_n \tag{40}$$

D'Arsonval Movement

Equivalent Electrical Circuit to Represent Instrument

The equation of motion of the movement may be written:

$$J\frac{d^2\theta}{dt^2} + B\frac{d\theta}{dt} + T\theta = U_A i_A \tag{41}$$

and the equation of the coil circuit of the instrument is:

$$R_A i_A + U_A \frac{d\theta}{dt} = e(t) \tag{42}$$

in any consistent system of units. In the centimeter-gram-second electromagnetic system, each term in equation 41 represents a torque in dyne centimeters, and in equation 42 each term represents a voltage in abvolts. Let:

J = moment of inertia of instrument movement—grams (centimeter)^2/radian
B = damping coefficient—dyne centimeters seconds/radian
T = spring constant—dyne centimeters/radian
θ = deflection—radians
I_A = coil current—abamperes
t = time—seconds
R_A = coil resistance—abohms
$e(t)$ = voltage across coil terminals—abvolts
U_A = electromechanical coupling coefficient—dyne centimeters/abampere or abvolts/radian/seconds
$U_A = 2B'LNr_2$ where
B' = flux density in air gap of magnet—gauss
L = length of coil side—centimeters
N = number of turns in coil
r_2 = coil radius—centimeters

Changing the units in equation 41 from dyne centimeters to grams force at the end of the pointer, and changing the deflection from radians (θ) to per unit of full scale deflection (X) and the coil current to amperes:

$$\frac{kJ}{980\,r}\frac{d^2X}{dt^2} + \frac{kB}{980\,r}\frac{dX}{dt} + \frac{kT}{980\,r}X = \frac{U_A}{9{,}800\,r}i \tag{43}$$

Making similar changes in equation 42 as well as changing the units of the equation from abvolts to volts:

$$R_c i + \frac{U_A k}{10^8}\frac{dX}{dt} = E(t) \tag{44}$$

Equations 43 and 44 may be written:

$$M\frac{d^2X}{dt^2} + C\frac{dX}{dt} + KX = U_M i \tag{45}$$

$$R_c i + U_E \frac{dx}{dt} = E(t) \tag{46}$$

M = equivalent mass at end of pointer—gram (seconds)^2
C = equivalent damping coefficient at end of pointer—grams seconds
K = equivalent spring constant referred to end of pointer—grams/full scale deflection
r = pointer length—centimeters
k = full scale deflection—radians
X = deflection—per unit of full scale = θ/k
i = coil current—amperes
t = time—seconds
R_c = coil resistance—ohms
$E(t)$ = voltage across coil terminals—volts
U_M = electromechanical coefficient referred to mechanical side—grams force at end of pointer per ampere of coil current = $U_A/9{,}800\ r$
U_E = electromechanical coefficient referred to electrical side—volts per unit velocity of pointer = $U_A k\ 10^{-8}$

$$\frac{U_E}{U_M} = \frac{10^8}{98kr}$$

or

$$U_E = U_M kr 9.8\times10^{-8} = U_M l 9.8\times10^{-8} \tag{47}$$

where $l = kr$ = scale length in centimeters.

Equations 45 and 46 may be represented by the equivalent circuit shown in Figure 15. Mathematically the integral of the voltage v across the three parallel branches corresponds with the displacement. But since the current i_L also is proportional to the same integral, the current i_L may be considered analogous with the displacement by applying the conversion factors indicated in Figure 15. The impedance of the equivalent circuit between the terminals where $E(t)$ is applied is the same for all frequencies as that of the actual instrument. The equivalent circuit may be connected into any electrical network to replace the actual instrument and the effect on the electrical network will be the same. The current i will be the same as that taken by the actual instrument, and the current i_L will be proportional to the value the instrument records on its chart.

Determination of Instrument Constants by Test*

K may be determined by measuring the force at the end of the pointer for full scale deflection by means of a delicate spring balance.

U_M may be found by applying a constant known current to the movement and noting the deflection.

$$U_M = KX/i \text{ under these conditions} \tag{48}$$

For an ammeter, U_M may be computed directly from K and the full scale current rating of the instrument.

$$U_M = K/I_{fs} \text{ where } I_{fs} = \text{current for full scale deflection} \tag{49}$$

f_n and β may be found from a transient record making use of the curves of Figures 8 or 9. If the damping is less than 0.707, f_n and β may be obtained from a steady-state a-c frequency response curve. The response at the resonant frequency may be measured and β computed from equation 11. f_n may then be obtained from equation 10. If the phase can be measured readily, f_n may be determined by applying an a-c signal and changing the frequency until the swing of the pointer is 90 degrees out of phase with the applied signal. This latter method of

* Since this paper was written, a bridge method of measuring instrument parameters accurately has been presented by R. W. Gilbert and W. N. Goodwin, Jr.[13-14] The latter paper develops the theory upon which the method is based, and the former paper describes the equipment and technique of using it.

determining f_n is independent of the relative damping.

M may be obtained from f_n and K. This equation follows from equation 8.

$$M = K/(2\pi f_n)^2 \tag{50}$$

C may be obtained from f_n, M, and β. By combining equations 4 and 5:

$$C = C_c\beta = 4\pi f_n M\beta \tag{51}$$

U_E may be obtained from U_M.

$$U_E = 9.8lr\,U_M \times 10^{-8} = 9.8lU_M \times 10^{-8} \tag{47}$$

Damping Reflected From the Electrical Side

If the electrical resistance of the coil circuit (coil resistance plus the driving point resistance of the network into which the instrument is connected) is represented R_T, and if the motion of the movement is considered after a condition when $E(t)$ is suddenly made zero, equation 46 becomes:

$$R_T i + U_E\frac{dX}{dt} = 0 \tag{52}$$

Combining this equation with equation 45 to eliminate i

$$M\frac{d^2X}{dt^2} + \left(C + \frac{U_E U_M}{R_T}\right)\frac{dX}{dt} + KX = 0 \tag{53}$$

or in non-dimensional form

$$(p^2 + 2\beta\omega_n p + \omega_n^2)X = 0 \tag{12}$$

where ω_n is given by equation 4 and

$$\beta = \frac{C + U_E U_M/R_T}{C_c} = \beta_o + \frac{U_E U_M}{2R_T\sqrt{MK}} \tag{54}$$

$$\beta = \beta_o + \frac{\pi U_E U_M f_n}{R_T K} \tag{55}$$

Thus the relative damping consists of two components, β_o which is the relative damping inherent in the movement when the coil circuit is open, and β_1 which arises by virtue of current flowing in the coil when the coil circuit is closed.

$$\beta = \beta_o + \beta_1 \tag{56}$$

$$\beta_1 = \frac{\pi U_E U_M f_n}{R_T K} \tag{57}$$

Making use of equations 47 and 49

$$\beta_1 = \frac{9.8\pi lKf_n \times 10^{-8}}{I_{fs}^2 R_T} \tag{58}$$

β_1 = component of relative damping reflected from the electrical side

l = scale length—centimeters

K = force required at end of pointer to cause full scale deflection against the action of the restoring spring—grams

f_n = undamped natural frequency of the movement—cycles per second

I_{fs} = full scale current of instrument—amperes

R_T = resistance of instrument coil plus driving point resistance of the network which is connected across the coil terminals of the instrument—ohms

Equation 58 may be written

$$\beta_1 = F/R_T \tag{59}$$

where

$$F = \frac{9.8\pi lKf_n \times 10^{-8}}{I_{fs}^2} \tag{60}$$

Equation 59 is plotted in Figure 10 with β_1 as a parameter.

An experimental verification of equations 58 and 59 was made on two typical d-c milliammeters, the transient characteristics of which are shown in Figures 11 and 12. The relative damping was determined for each of the circuit conditions by Methods 1 and 2 described in the paper. The values of β_1 then were computed by equation 56. The resultant values then were plotted in Figures 16 and 17. These curves indicate also the relative degree of accuracy that can be expected in the determination of β by these methods.

Simplified Analysis of Servo-Actuated Recorders

For most servo-actuated recorders the proportional range of the servomechanism of the instrument corresponds to only a small fraction of the total scale range of the instrument. If a signal corresponding to more than this proportional range is applied, the pointer or pen will move across the chart at some maximum velocity, which depends upon the design of the instrument, until the proportional range is reached. For this type of instrument, the upper limit of frequency response is fixed by the maximum velocity of the recording pen. This maximum velocity is specified usually in terms of the time required for the pen to move from one end of the chart to the other under the influence of a signal large enough to give maximum pen velocity. The symbol T_{fs} is used to represent this quantity.

Since the fundamental limitation of response of such instruments arises from the fact that the pen velocity across the chart cannot be greater than some maximum value, the upper limit of frequency response depends upon the peak-to-peak amplitude of the signal as recorded on the chart. The symbol A is used to represent the peak-to-peak chart amplitude expressed as a fraction of the total chart width.

If the basic type of signal is taken as a sine wave of frequency f and period T, and the center of the chart is taken as a deflection reference:

$$X = (1/2)A \sin(2\pi ft) \tag{61}$$

and the maximum velocity on the chart will be

$$\left.\frac{dX}{dt}\right]_{max} = \pi fA = \frac{\pi A}{T} \tag{62}$$

This cannot be greater than $1/T_{fs}$ if the instrument pen is to be able to follow the changing signal.

$$1/T_{fs} > \pi fA = \pi A/T \tag{63}$$

$$A < 1/\pi fT_{fs} = (1/\pi)(T/T_{fs}) \tag{64}$$

or

$$T > \pi A/T_{fs} \tag{65}$$

If this condition is satisfied, the error in the record obtained at any point will not be greater than the proportional range of the servomechanism of the instrument. Expression 65 is plotted in Figure 18 with T_{fs} as a parameter.

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No Discussion

The Locomotive Traction Generator Comes of Age

RICHARD LAMBORN
MEMBER AIEE

OVER 2,100,000 horsepower of Diesel-electric locomotives were produced in the United States during 1947. Other internal power locomotives, such as steam-turbine electric or gas-turbine electric and motor-generator locomotives, were built in very small quantities, but for our purposes may be added to the above total. All of these locomotives incorporate d-c traction generators of specialized design. Taken together, they account for the surprising figure of 70 per cent of the total d-c generator production of the entire United States for the year.

Compact, Lightweight Design

For a generator which is to be applied to locomotive service, small physical size (especially minimum over-all length) and light weight are items of paramount importance. These two requirements lead to a number of differences in design proportions as compared with the conventional generator built to spend its lifetime in a more or less fixed location. Traction generators are commonly designed with a large number of poles in relation to their capacity, since such machines tend to be relatively light in weight, and have short end windings and short commutators. Further shortening of the commutator by accepting higher than normal current density in the carbon brushes can be accomplished, provided the commutation factors and commutator surface conditions are made sufficiently favorable. Class *B* heat-proof types of insulation are used in all locomotive traction equipment because of the very considerable reduction in amount of active material made possible by the 120 degrees centigrade permissible continuous operating temperature rise.

All features of the mechanical design are made as simple and lightweight as torsional vibration conditions, buffing shock loads, and general over-all stiffness considerations will permit. Length required for purely mechanical functions is held to the minimum by building single-bearing machines having an antifriction bearing at the commutator end, and a bolting rabbet at the fan end of the armature spider to permit attachment to the driving flange of the prime mover. Such generators are often completely end-bell supported which makes a short, reasonably large diameter or "pancake" design much to be preferred to a longer, smaller diameter or "sausage" design, even though the latter might be slightly less expensive to manufacture.

Typical locomotive traction generator construction is illustrated in Figure 1, which shows the side view of a 2,000 horsepower Diesel-electric locomotive engine-generator set and by the section drawing, Figure 2, which shows the proportions of a somewhat smaller generator also built for end-bell mounting.

Concepts of Direct Current "Kva" and "Wrap-around"

When operating at rated load, prime movers such as the Diesel engine or the gas turbine are fundamentally constant torque output devices. Railroading, on the other hand, demands high-power outputs over a wide range of operating speed, which involve a widely variable torque demand. It is the flexibility and reliability with which electric drive can provide the required variation and multiplication in torque between the prime mover and the driving axles that account for the phenomenal growth of this branch of the electrical industry.

The required electrical size or D^2L (where D = diameter of the armature core and L = stacked height of the armature core) of a generator in stationary applications can usually be fairly accurately estimated from the horsepower and speed of the prime mover. However, the situation is somewhat more complicated in the case of generators applied in traction service. As a means of contributing their share to the torque range of the electric transmission system, traction generators are equipped with a means whereby their excitation is automatically regulated to make them deliver their full kilowatt output at high voltage and low current as well as at low voltage and high current, as indicated in Figure 3.

The specific range of volts and amperes over which such a machine is required to operate continuously becomes an important factor in determining its size. For instance, a generator driven by a Diesel engine delivering 1,500 horsepower net input will develop approximately 1,040 kw output, and in locomotive service this output may be required on a continuous basis anywhere within the range between 1,000 volts at 1,040 amperes to 500 volts at 2,080 amperes. In other words, the generator must be large enough to rate 1,000 volts and to also rate 2,080 amperes, although these do not occur simultaneously. In such a case, stating the capacity in terms of maximum rated volts times maximum rated amperes per 1,000 or $1{,}000 \times 2{,}080/1{,}000 = 2{,}080$ "kva" comes much closer to defining the size or D^2L required than the more conventional method of stating generator capacity in terms of kilowatts.

This same requirement may be expressed in a slightly different form by de-

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RICHARD LAMBORN is a designing engineer of the transportation motor engineering division, General Electric Company, Erie, Pa.

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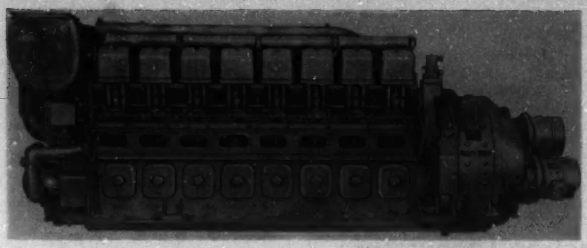

Figure 1. Side view of 2,000-horsepower Diesel-electric locomotive engine-generator set

termining the ratio of maximum rated amperes to amperes at maximum rated volts or 2,080/1,040 = 2, and defining the generator size by saying it is a 1,500-horsepower input machine with a two to one continuous rating "wrap-around"; that is the range of currents over which the generator output must be extended or wrapped around the constant input volt-ampere hyperbola. The amount of wrap-around rating required in the generator of an internal powered locomotive depends chiefly on such factors as horsepower per ton on drivers, maximum locomotive speed at which full power is developed, traction motor characteristics, and motor control connections or combinations used. The relative effect of each of these factors can best be understood by working through a typical example with reference to Figure 4.

How to Provide Wheel Horsepower Over a Range of Locomotive Speed

Let us assume we wish to determine the amount of generator continuous rating wrap-around required on an 80-miles per hour Diesel-electric road-freight locomotive weighing 115 tons, having four axles, all motored, and equipped with a Diesel engine capable of developing 1,500 horsepower net for traction. To start with, general practice in freight locomotive service is to provide a maximum tractive effort for acceleration purposes equal to 30 per cent of the locomotive weight, which in our case would be $0.30 \times 115 \times 2000 = 69{,}000$ pounds. At the other end of the operating range, full available engine power should be transmitted to the rail up to at least 90 per cent of the maximum permissible speed, or $0.90 \times 80 = 72$ miles per hour. Assuming 93 per cent as a typical value of efficiency for the generator and 90 per cent efficiency for the traction motors, the tractive effort corresponding to this speed would be $0.93 \times 0.90 \times 1{,}500 \times 375/72 = 6{,}540$ pounds. From these two calculations we obtain the range in tractive effort or torque values which must be handled at full power by the electrical transmission equipment as $69{,}000/6{,}540 = 10.55$ to 1.

Since the torque developed by a motor is equal to some constant multiplied by flux and by armature current, it follows that the product of motor flux times motor current must be capable of variation over a 10.55 to 1 range to satisfy the requirements of our hypothetical locomotive application. On the average, a typical railway traction motor operating at rated horsepower would probably be limited to a 2.5 to 1 flux change by either heating, commutation, or flashover considerations. This means that the power delivered to our motors must be varied over a $10.55/2.5 = 4.22$ to 1 range of current values in order that they may be able to successfully handle full output over the required 10.55 to 1 range of torques.

A 1,500 horsepower generator capable of delivering its full kilowatt rating over a 4.22 to 1 range of currents would be a very large and heavy machine indeed. This condition can be greatly improved by providing a few relatively lightweight contactors to connect the traction motors in two parallel groups of two motors in series during acceleration and low speed operation, and to later reconnect them so as to place all four motors in parallel across the generator terminals for the higher operating speeds. This reduces the required generator current output range by a factor of two without in any way affecting the range in current flowing through the

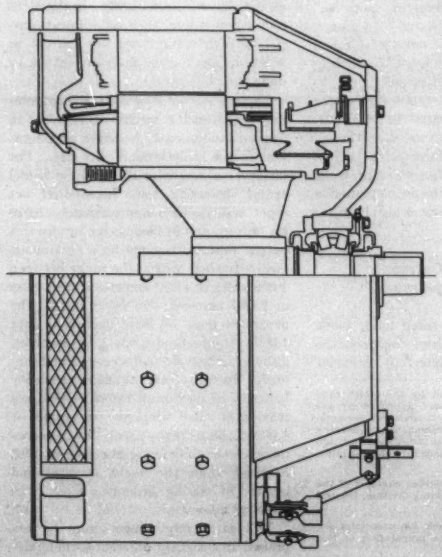

Figure 2. Half section of typical locomotive traction generator designed for end-bell mounting

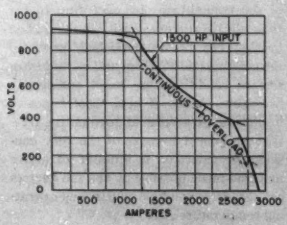

Figure 3 (right). Locomotive traction generator volt-ampere output characteristic

windings of each traction motor. The use of series-parallel motor connections has thus brought us down to a $4.22/2 = 2.11$ to 1 range of volts and amperes over which the generator must deliver full power.

Generator Heating

Let us now consider the range of volts and amperes over which the generator must rate continuously as contrasted to the 2.11 to 1 range over which we have determined that it must be able to handle full power.

Road freight locomotives are conventionally designed to have a continuous tractive effort rating of 15 to 16 per cent of their weight on powered axles. From the point of view of the series-excited traction motors, since their flux must decrease with decreasing load, the current required to develop 15 per cent adhesion must be more than half that required to develop 30 per cent adhesion. It follows that the continuous traction motor rating must occur in the series-parallel motor connection. Hence the maximum current that the generator must handle on a continuous basis will be required immediately following the transition from the series-parallel to the full-parallel motor combination. It would theoretically be exactly twice the current corresponding to the maximum rated voltage point of the machine. However, the maximum continuous rated load is often chosen as about 90 per cent of this value because the resultant saving in size and weight of generator is considered more worthwhile than whatever insulation life may be sacrificed from such over-temperatures as occur during the occasional periods of operation in the resulting small overload current zone. Thus we arrive at $0.9 \times 2 = 1.80$ to 1 range of volts and amperes as the continuous rating wrap-around required of the traction generator. To recapitulate, the ranges over which the electric equipment must handle full power on our hypothetical locomotive are:

1. 10.55 to 1 for traction motor torque.
2. 4.22 to 1 for traction motor current.
3. 2.11 to 1 for generator current.
4. 1.80 to 1 for continuous rated generator current.

Other Control Combinations Are Also Used

Frequently more than one feasible solution can be found to the problem of designing the transmission system for an internal-power locomotive, and the best solution will not necessarily be the one involving the smallest size generator. If using a single traction motor connection involves a big heavy generator, as in our example, the addition of series-parallel control to reduce the demand on the generator will effect a very appreciable saving in the cost and weight of the over-all transmission. But as smaller sizes of apparatus are approached in lower horsepower locomotives, a point will be found where the additional cost of a larger size generator required by the use of a single motor combination will be largely offset by savings in first cost and installation cost of those control items which are no longer required. Quite naturally, the choice will be biased in favor of the single motor connection alternative by consideration of such customer advantages as lower maintenance because of the simpler control equipment and improved service performance due to the locomotive being less "slippery" with only half as many motors in series under the high tractive effort conditions.

Although the generator applied in a single-motor connection transmission system always requires increased overload commutating ability, the chief pitfall in designing such a machine is more apt to be one of heating. Generally speaking, it is not adequate to design such a generator to have a continuous current rating corresponding to the combined continuous rated current of the traction motors in their operating combination. If this were done, it would be found that the generator could readily become badly overheated during operation at its maximum overload current while the traction motors were still within acceptable temperature limits. This to a large degree results from the fact that the load loss in the motor armature will be very low under such conditions because of its very low speed when exerting high torques, while the load loss in the generator armature will be very high because it may be running at or near its maximum speed. To be safe, a generator on a single motor combination locomotive should have a 15 or 20 per cent higher continuous ampere rating than the combined continuous ratings of the motors it supplies.

The optimum relative proportions of any specific locomotive transmission are likely to be distorted or compromised to some degree by the practical necessity of choosing from existing standard sizes of apparatus. Nevertheless, several general trends can be noticed in the data in Table I which presents a breakdown of the over-all transmission characteristics of a number of typical Diesel-electric locomotives. Item *B* represents our hypothetical locomotive which, except for an added complication in the control that was omitted from our example for the sake of simplicity, is identical with a currently very popular design of road freight locomotive. The other five items are all actual locomotives being manufactured today.

The first two locomotives listed serve to emphasize the fact that a generator used with a 2-to-1 series-parallel traction motor combination requires a 1.8-to-1 continuous rating "wrap-around." Using either an additional control connection on a 4-motor locomotive in which the motor fields are shunted in series-parallel before going into full parallel (Item *D*), or a 3-to-2 series-parallel combination on a 6-motor locomotive (Item *C*), serves to

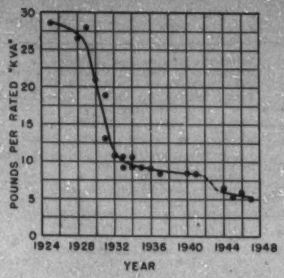

Figure 5. Weight trend of locomotive traction generators

Figure 4. Steps in determining "wrap-around" requirement of traction generator for 115-ton 1,500-horsepower 80 miles per hour, Diesel-electric freight locomotive

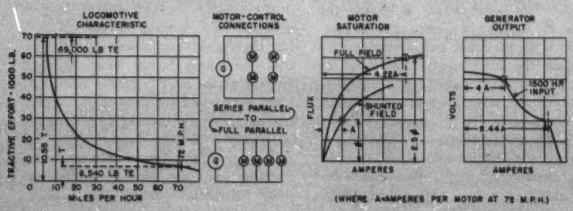

Table I

Item	Locomotive Specifications, Including Horsepower for Traction; Weight on Drivers; Maximum Speed at Full Power; and Traction Motor Connections Used	Traction Motor Torque Range	Traction Motor Flux Range	Traction Motor Current Range	Generator Current Range	Continuous Rated General Current Range
A	600 hp; 70 tons; 50.5 mph utilization; 2S2P FS-1, 4P FS-1, 4P FS-2	11.20 to 1	2.69 to 1	4.16 to 1	2.08 to 1	1.82 to 1
B	1500 hp; 115 tons; 72 mph utilization; 2S2P FS-1, 4P FS-1, 4P FS-2	10.55 to 1	2.50 to 1	4.22 to 1	2.11 to 1	1.80 to 1
C	900 hp; 93 tons; 50.5 mph utilization; 3S2P FS-1, 2S3P FS-1, 2S3P FS-2	9.37 to 1	2.76 to 1	3.39 to 1	2.26 to 1	1.48 to 1
D	2000 hp; 110 tons; 67 mph utilization; 2S2P FS#1, 2S2P FS-2, 4P FS-1. 4P FS-2	6.45 to 1	1.95 to 1	3.30 to 1	1.65 to 1	1.49 to 1
E	2×210 hp; 80 tons; 16 mph utilization; 2 (2P) FS-1; 2 (2P) FS-2	6.26 to 1	2.29 to 1	2.73 to 1	2.73 to 1	1.90 to 1
F	2×400 hp; 51 tons; 50.5 mph utilization; 2 (2P) FS-1, 2 (2P) FS-2, 2 (2P) FS-3, 2 (2P) FS-4	6.56 to 1	2.94 to 1	2.23 to 1	2.23 to 1	1.59 to 1

Abbreviations designating motor connections are S = Series; P = Parallel; FS-1 = Full exciting field strength; FS-2 = Reduced field strength; FS-3 = Further reduced field strength; etc. For example 2S2P FS-1 indicates four motors operating on full field strength connected with two parallel groups of two motors each in series connected across the generator terminals.

reduce this required rating "wrap-around" to approximately 1.5-to-1. Translated into physical terms, this makes possible a reduction of approximately 15 per cent in the amount of active generator material required as compared with the straight 2-to-1 series-parallel type of control. No general rule for the required generator rating wrap-around on single motor combination locomotives (Items *E* and *F*) should be attempted, as it depends as much on the characteristics of the particular traction motor used as on any other factor.

A study of the tabulation as a whole serves to again point out the fact that for a locomotive of given weight and fixed maximum full-power speed, the required traction motor-torque range is inversely proportional to the prime mover horsepower. From here it is but a simple step to the realization that if weight and utilization speed are established, increasing the horsepower will not involve a proportionate increase in the size of the electrical transmission equipment.

Switcher Generators Differ From Road Locomotive Generators

Thus far, the entire discussion regarding ratings has referred to what is commonly classed as road-locomotive type of service which implies that all the equipment has been designed to permit sustained full power operation. While this is by far the more glamorous field for the design and application engineer, there are two other distinct classes of service for which Diesel-electric locomotives are built in large quantities. One of these is railroad yard switching service. Because of the low-load factor in this type of operation occasioned by the frequent off-and-on nature of the power requirements and many light-load movements, it is permissible to design such an electrical transmission with somewhat reduced ratings and to rely upon the reduced load factor and low rms effective current to prevent overheating of the equipment.

Locomotives designed for use by industrial plants constitute the third major classification. Many industrial railroad systems have such a small total length of track that it is almost impossible to think of service on them in terms of continuous rating or continuous operation. In fact more than 600 such locomotives of one particular design have been operating successfully for years with generators so small in size that they cannot be operated continuously at any point on their full horsepower volt-ampere characteristic curve without exceeding AIEE Class *B* permissible temperature rises. Designing for such special applications should of course be based on actual service experience whenever possible rather than on any generalized rules such as here developed for the road locomotive type of service.

Various Means Employed to Produce Hyperbolic Volt-Ampere Curve

Another factor which influences the size and weight of a generator to some extent is the particular means employed to obtain the automatic regulation of its output over the required range of current and voltage. There are at least five different schemes in general use, ranging from the simplicity of a self-excited generator which depends entirely on the stalling characteristics of a diesel engine for its ability to produce a wrap-around type of output curve, to the amplidyne exciter which, in conjunction with the electro-hydraulic governor, (1) prevents engine overloading or stalling at all engine speeds and has the further ability of adding predetermined voltage and current limit cut-offs (2) to the generator hyperbolic volt-ampere characteristic. The self-excited generator with stalling control is used only on small-capacity power plants such as those designed for industrial service where minimum first cost is an important objective.

Second in order of increasing complexity is the scheme using a controlled saturation type of differential field exciter. It has been successfully applied to a large number of switching locomotives and does an excellent job of matching the generator's demand to the engine's output ability under one specific set of design conditions. However, it lacks ability to correct for variations in torque demand of the generator such as those caused by higher or lower than normal exciting field temperatures or for variations in torque ability of the Diesel engine due to fouled injectors, change in altitude, or normal wear and tear. The third scheme is an outgrowth of the second and, since it overcomes the difficulties just enumerated, it meets the essential requirements of an excitation system suitable for road type of service. It is characterized by the addition of an engine governor servo-operated rheostat in the battery field circuit of the controlled-saturation exciter whereby the excitation of the main generator is regulated so that its output is made to match the capabilities of the Diesel engine. Neither of these systems has an inherent generator overload current limit feature. The partial power volt-ampere characteristics tend to be undesirably flat for optimum locomotive accelerating performance, and the excitation tends to be unnecessarily high at the lighter loads beyond the point where the volt-ampere curve has departed from the constant horsepower portion of the characteristic.

The fourth scheme uses no exciter, but requires three exciting field windings in the generator; namely, self, separate, and differential, with a governor servo-operated rheostat in the battery-excited separate field winding circuit. Naturally, the rheostat and its servo-mechanism must be much larger when working directly on the main generator field than when working on the field of an exciter. In such a generator the field structure must be relatively deep to accommodate the greater gross number of ampere turns resulting from the use of an opposing series differential winding, and to allow

for the extra amount of insulation needed when using a multiplicity of field windings.

The fifth scheme based on the use of the amplidyne type of exciter requires the smallest capacity of servo-operated rheostat, produces the most desirably shaped family of partial power characteristic curves, and by providing definite overload and over-excitation protection, permits the designer to use the minimum of material in the construction of the generator stator parts.

Other Functions Performed by Traction Generators

In addition to the primary function of providing power for propelling the locomotive, the traction generator usually has other secondary functions. On locomotives using Diesel engines as their prime movers, it is almost universal practice to "motor" the traction generator from a storage battery in order to crank the engine and get it started. In the case of the gas-turbine type of power plant, supplying power to the main generator promises to be the preferred way of bringing the turbine-compressor unit up to a self-sustaining speed as well as for turning it over slowly during the cool-down period required prior to the actual shutting down of a unit that has been up to operating temperature. Similarly on the motor-generator set type of trolley locomotive, motoring the traction generator from some auxiliary source of d-c power is the preferred method of getting the synchronous motor above half speed where the single phase power can effectively accelerate and pull it into step. A separate series winding is built into the generator for this starting duty on small and medium sized machines, but in the largest size units the series winding is sometimes omitted and control equipment added to permit the use of the machine's regular separate excitation winding for the motoring operation.

A more recent function, which at the present time is required only on locomotives used in road freight or road passenger service, is that of providing excitation for the traction motors when they are operating in their braking connection. Electric braking is accomplished by connecting the traction motor armatures to a set of forced air-cooled grids, and regulating their excitation to suit the braking requirements of the train. The separate excitation power for these motor series fields is required at a relatively low voltage, and is obtained from the traction generator while being driven at the minimum operating, or idling, speed of the prime mover. This requirement seldom involves any heating problem, but may offer one in stability of operation, as proper equalization of the load between similar units operating in multiple from a single head-end control station is a universal requirement. Arranging the traction-motor field excitation circuit to include the generator cranking winding so that it will act as a differential series exciting winding is one means of at least improving the equality of load between units.

Traction generators have at times been used to furnish at least a portion of the auxiliary power requirements of the locomotive. In certain switcher locomotives they have been arranged to charge the storage battery whenever the engine was idling, with the traction motors consequently disconnected from the generator circuit. In other cases, motor driven air compressors or blowers have been operated from the variable voltage delivered by the generator while it was supplying power for traction. Such practices, however, have never been and probably never will be universally accepted by the industry.

Striking Progress in Design Is Continuing

The first commercial Diesel-electric locomotive in the United States was built in 1925, but as of that date the design proportions of the traction generators had not departed very greatly from those used in industrial practice. During the following ten-year period a three-to-one reduction in weight per kilovolt-ampere of continuous rating was accomplished as indicated by the data of Figure 5. This enormous technological advance was due in part to the engine builders' increase in the horsepower output and speed of their prime movers, but to a much greater extent, it was the result of the efforts of the electrical industry in developing the highly specialized designs of generators according to the principles that we have been discussing. Although the designs of locomotive generators were frozen during World War II, the curve of progress is seen to have flattened out to a great extent even before the war period. Since the war the curve has taken a new dip toward still lower weight per kilovolt-ampere. This second, though materially smaller, advance is again due in part to the availability of higher speed, higher horsepower, prime movers and in part to the electrical industry's further improvements in insulations, excitation systems, and motor control techniques for road locomotive application.

Conclusion

In the 60-year old electric-traction industry, the 22-year old locomotive traction generator is a comparative youngster, but on reaching maturity it has assumed a role of major importance. Business in these generators in 1948 promises to exceed that of 1947, with a probable production of 2,500,000 horsepower. There has been striking progress in design through the years and the future holds promise of further interesting developments with a continuing trend toward higher speed, higher horsepower, and still more output per pound or per cubic foot.

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No Discussion

Formation of Insulating Oxide Films on Electrical Steel

P. L. SCHMIDT
ASSOCIATE AIEE

General

IN order to reduce eddy current loss to a minimum, the magnetic cores of electric machines designed to operate on alternating current are assembled in laminated form from silicon steel punchings which are insulated from each other by the application of special coatings to the surfaces of the sheets or laminations. Various types of varnishes and films produced by a chemical reaction are being used as insulating coatings but most of these fail to meet one or more of the requirements of a satisfactory interlaminar surface treatment. The important requirements of an insulating film are low cost, ease of application, small decrease in space factor in the core, high electrical resistance, high corrosion resistance, and good paint adherence.

Insulating films which fulfill these requirements can be produced by heat treating punchings to form an oxide on the surface of the steel. The problem is to obtain consistently a tightly adherent, thin oxide having a high electrical resistance. Obviously, the formation of ferric oxide, common red rust, must be avoided because it is thick and loose, thereby resulting in an undesirable decrease in space factor and presenting handling problems.

It has been found that thin, tenaceous, highly resistant films of magnetic oxide can be formed by heat treating punchings in a batch process at 400 to 550 degrees centigrade in a steam atmosphere containing not more than 0.5 per cent free oxygen. On one per cent silicon steel this treatment produces a film which has a median interlaminar resistance of at least 20 ohms centimeters squared per lamination and which decreases the space factor less than one per cent. Interlaminar resistance measurements are usually made on rotor punchings about four inches in diameter in accordance with the procedure outlined elsewhere.[1]

The batch-type process which has been highly successful consists of placing the charge in a bell-type or car-type batch furnace and covering it with a sand sealed metal inner cover. During the oxidation part of the cycle, steam is introduced into the inner cover and allowed to diffuse out through the sand seal. A more practical production method may be carried out in a conventional continuous furnace at temperatures between 400 and 550 degrees centigrade provided that steam is supplied to and free oxygen excluded from the oxidizing zone of the furnace.

When attempts were made to oxidize laminations in conjunction with a strain annealing process in a continuous-type furnace, a procedure which is very desirable commercially, it was found that films with satisfactory physical properties could be obtained using an atmosphere composed of CO, CO_2, H_2, H_2O, and N_2. However, these films were found to have an interlaminar resistance approximately 1 per cent of that obtained by oxidation in a steam atmosphere at 500 degrees centigrade.

The following experimental work was undertaken to determine the difference between oxide films having substantially the same bluish-gray characteristic color and the same desirable physical characteristics, such as thinness, toughness, and tenacity, but high electrical resistance on one hand and low resistivity on the other.

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P. L. SCHMIDT is with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

Background

A continuous process set up for experimentally strain annealing and oxidizing stator punchings for small motors was carried out in a pusher-type continuous furnace consisting of a heating zone, controlled cooling zone, and a fast cooling zone. Combusted gas generators which may be adjusted to give various proportions of CO, CO_2, H_2, H_2O, and N_2, supply a controlled atmosphere to this furnace, and, by varying the amounts of the reducing and oxidizing components of the gas at a given temperature, either a reducing atmosphere or an oxidizing atmosphere may be supplied to the furnace.

It is claimed to be theoretically possible[2] to obtain a combusted gas mixture which is reducing at annealing temperatures of approximately 725 degrees centigrade and oxidizing at lower temperatures. However, the range of component gases over which this may be possible is very small so that variations in the composition of the gas supply or in the temperature of the cooling water of the combusted gas generator causes large variations in the oxide film produced. The first method of annealing and oxidizing in the continuous furnace was carried out in an attempt to obtain this type of atmosphere. It was believed that the punchings would be protected from high temperature oxidation which causes undesirable sticking and which is difficult to control, and that an oxide film would be produced on the punchings when they reached the controlled cooling zone without causing undue sticking or too great thickness of film.

Furnace temperatures from 600 to 725 degrees centigrade, combusted gas atmospheres ranging from the richest mixture possible with the available equipment to very lean atmospheres with excess oxygen, and dew points ranging from 10 to nearly 100 degrees centigrade were tried.

Figure 1. X-ray diffraction pattern of an oxide film of low-electrical resistance

Summarizing the results of these trials, it appeared that:

1. High dew points in the high temperature zone with either rich or lean combusted gas caused undesirable sticking.

2. Lean, dry combusted gas gave a film which was too thick, usually flaked off, and had very low interlaminar resistance.

3. Rich, dry combusted gas produced films of very low resistance partly because they were not much thicker than temper films.

4. On rare occasions a batch of punchings would be produced which had a tenaceous, thin, blue oxide film which gave high interlaminar resistance.

In regard to the fourth point just mentioned, poor results were also obtained under seemingly exact conditions of temperature and atmosphere. This indicated that although the proper conditions in the furnace may be possible, it is very unlikely to consistently achieve these conditions in production. It was found that a great deal of variation existed in the punchings annealed in this manner.

It was then decided to supply a rich, dry combusted gas atmosphere to the high-temperature zone of the furnace and to add water vapor into the controlled cooling zone. By admitting the combusted gas atmosphere into the high-temperature zone only and by closing the gas exhaust port at the charging end of the furnace, it was believed that the large flow of gas toward the exit end of the furnace would prevent the back diffusion of water vapor into the high-temperature zone. The purpose of this method was to prevent oxidation of the laminations entirely while at high temperature, and to promote oxidation in the controlled cooling zone after they had cooled to temperatures where the oxidation process could be better controlled.

It has been found that the product obtained by this method was fairly consistent so far as physical appearance was concerned, that the films had a grayish appearance and a median resistance of approximately 0.2 ohm centimeters squared per lamination which is slightly better than that obtained by the previous method.

Although the combusted gas and steam process produces oxide films which have fairly satisfactory and consistent physical characteristics and which have sufficiently high interlaminar resistance for punchings which are 3/4 inch wide behind the teeth, two problems still existed. The problems were:

1. What is the fundamental chemical or physical difference between these low resistivity oxide films which are produced in conjunction with the strain anneal and the high resistivity films which can be produced in steam between 400 and 550 degrees centigrade, both of which have substantially the same physical appearance?

2. Can a process be set up for continuous oxidizing and annealing such that a thin, tightly adherent oxide film of high electrical resistance will be obtained?

Investigation of High-Resistance and Low-Resistance Oxide Films

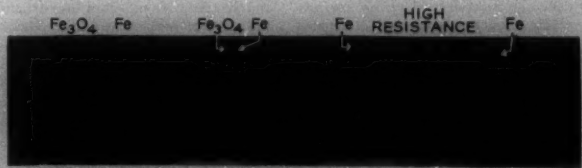
A great deal of development work on the difference between the high-and-low-resistance films has been done and a satisfactory solution to the problem has been obtained. In order to obtain the information concerning the difference between high-resistance films and low-resistance films, a comparative study of the two types of oxides was made by X-ray diffraction analysis. The first method used was to obtain an X-ray diffraction pattern of the coatings by reflecting X rays from the coating on the steel. A diffraction pattern from the bare steel was then subtracted from the pattern obtained from the steel and film together. Using this method, a sample of one per cent silicon steel which was annealed at 725 degrees centigrade and oxidized at 500 degrees centigrade in a lean combusted gas atmosphere and also a sample of one per cent silicon steel which was steam oxidized at approximately 500 degrees centigrade were examined. This method of analysis indicated that the oxide formed in combusted gas at 725 degrees centigrade and the oxide film formed at 500 degrees centigrade in a steam atmosphere were essentially Fe_3O_4 and contained a slight amount of Fe_2O_3.

It happened that the film which was formed at the higher temperature could be stripped off rather easily by bending the sample, so it was decided to make an X-ray study of the oxide powder itself. The results of this test showed that the oxide film contained a large amount of metallic iron. This showed that the iron lines which were present in the first analysis were not only due to the base metal, but were also caused by metallic iron in the film itself. This was the first clue to a possible difference between the high-resistance and low-resistance oxide films.

This experiment was now repeated with samples from the annealing and oxidizing process using combusted gas and water vapor as described before, and equivalent samples oxidized in a steam atmosphere at 500 degrees centigrade. It was found that the film formed in the continuous furnace contained a large amount of iron, and the low-temperature oxide film formed in a steam atmosphere contained very little metallic iron. Figure 1 shows an X-ray pattern of the oxide film removed from a typical punching which was annealed and oxidized in the continuous process. Figure 2 shows an X-ray diffraction pattern of the oxide produced in the steam atmosphere at 500 degrees centigrade. It will be noted that in the case of the former oxide, the Fe lines are very prominent and the Fe_3O_4 lines are in comparison very weak. The reverse is evident in the figure showing the X-ray diffraction patterns of the high-resistance film; namely, the Fe_3O_4 lines are prominent and the Fe lines are faint.

The distribution of interlaminar resistance corresponding to the films of Figures 1 and 2 are plotted as curve *B* and curve *A* respectively in Figure 3. Samples

Figure 2. X-ray diffraction pattern of an oxide film of high-electrical resistance



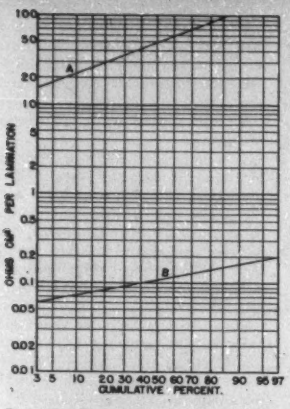


Figure 3. Distribution of resistance values

A—Tests made of 12-inch squares oxidized in a steam atmosphere at 500 degrees centigrade for four hours

B—Ten-inch square stator punchings, 3/4 inch wide behind teeth, annealed at 725 degrees centigrade in rich combusted gas, steam added in cooling zone to increase moisture to 15 per cent

for these tests were taken from the same heats as those from which the samples were taken for the X-ray diffraction analyses shown in Figures 1 and 2. Curve *B* has a median of 0.11 ohm centimeters squared per lamination and curve *A* has a median of 50 ohms centimeters squared per lamination which may have been as low as 25 ohms centimeters squared per lamination had the tests been made on punchings instead of squares.

A semiquantitative X-ray analysis of these films was then made. In order to do this, X-ray patterns of pure iron powder mixed with various amounts of Fe_2O_3 were obtained. Using these three patterns as standards for the different percentages of iron, the amount of iron in the high-and-low-resistance films could be estimated by the techniques employed in X-ray diffraction analysis work. It was found that the low-resistance films contained approximately 50 per cent metallic iron and the high-resistance films contained approximately 10 per cent free iron.

The reason for the presence of metallic iron in the low-resistance oxide films was found in the literature[3] on the subject. It is pointed out that under controlled oxidizing conditions above 570 degrees centigrade, iron oxidizes to form only ferrous oxide (FeO). When this oxide is subsequently cooled below 570 degrees centigrade, it becomes unstable and decomposes to form ferro-ferrosic oxide (Fe_3O_4) and metallic Fe. Below 570 degrees centigrade, FeO does not form to any great extent and thus films which are formed below this temperature do not contain an appreciable amount of metallic iron.

Method of Obtaining High-Resistance Oxides in a Continuous Process

The results of the X-ray diffraction analyses indicate that if it is desired to anneal laminations at temperatures of approximately 725 degrees centigrade and then to oxidize in conjunction with this annealing procedure, it is necessary to keep the punchings bright during the annealing cycle and during the time while they are cooled to a temperature below 570 degrees centigrade. The bright punchings can then be oxidized in a controlled atmosphere without the danger of forming FeO.

It is almost impossible to carry out this procedure in a conventional type of continuous furnace by introducing combusted gas into the annealing zone and steam into the latter part of the controlled cooling zone. There is enough back diffusion of steam even against a positive flow of gas from the charging end of the furnace to the exit, so that the punchings are oxidized above 570 degrees centigrade and FeO is formed.

A type of furnace such as that shown in Figure 4 would be necessary in order to produce the satisfactory oxide in conjunction with a continuous annealing process. This furnace consists of an annealing zone and an oxidizing zone separated by a gas-tight transfer chamber. A reducing atmosphere is maintained in the annealing zone and the transfer chamber, and an oxidizing atmosphere such as steam is supplied to the oxidizing zone. The punchings are cooled to a temperature below 570 degrees centigrade before entering the oxidizing chamber of the furnace, and the transfer zone is so arranged that it will serve to keep the oxidizing atmosphere from contaminating the reducing atmosphere in the annealing zone.

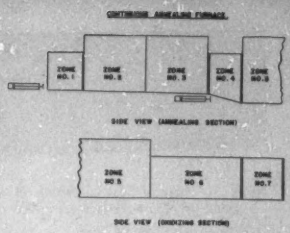


Figure 4. Continuous annealing and oxidizing furnace

Zone 1—Purging chamber, reducing atmosphere

Zone 2—Annealing zone, 700–850 degrees centigrade, reducing atmosphere

Zone 3—Controlled cooling from annealing to oxidizing temperature, reducing atmosphere

Zone 4—Transfer zone, reducing atmosphere

Zone 5—Oxidizing zone, 400–550 degrees centigrade, steam atmosphere

Zone 6—Cooling from oxidizing temperature to slightly above 100 degrees centigrade, steam atmosphere

Zone 7—Exit chamber, inert atmosphere

Conclusions

The results of this investigation show:

1. Low resistivity of oxide films is caused by conducting particles of metallic iron distributed throughout the film.

2. The oxide film which is formed in the conventional continuous annealing and oxidizing process contains a large amount of metallic iron caused by the oxidation occurring above 570 degrees centigrade. At this temperature FeO is formed which later decomposes into Fe_3O_4 and Fe.

3. In order to obtain a film which is not contaminated with metallic iron, it is necessary to carry out the oxidation below 570 degrees centigrade. If annealing as well as oxidation is desired, the punchings must remain bright during the annealing cycle and while they are cooling to the oxidizing temperature.

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No Discussion

The Increasing Uses for Electricity in Land Transportation

CHARLES KERR, Jr.
MEMBER AIEE

COMMERCIAL land transportation fundamentally falls into two major classifications, the transportation of passengers in urban communities and the intercity transportation of both passengers and freight. In the United States alone, this industry operates 175,000,000 installed horsepower for the movement of its traffic. Potentially, no other single industry offers the electrical manufacturers such a broad opportunity for expansion in the use of electric equipment.

Often fears have been expressed that new prime movers like the internal combustion engine, the steam turbine and, ultimately, the gas turbine will minimize the importance of electrical equipment to this industry. Actually, with one major exception, the exact opposite has been the case. Land transportation generally is becoming increasingly more dependent upon electricity, not only in motive power, but in those allied operations which are essential to the efficient and expeditious movement of traffic.

Figure 1 shows the extent to which electric equipment has been adopted by this industry, together with the changes which have occurred in a 20 year period. Of even greater significance is the rapid expansion during the past few years, illustrated by Figure 2. While total figures for 1947 are yet unavailable, sufficient information exists to indicate that motive power purchases alone in that year will equal all purchases during 1946. The present rate of expansion exceeds all previous experience.

Urban Transportation

Public transportation in urban centers is an essential service with problems of ever increasing perplexity. It can be supplied by the motor bus, the trolley coach, the street car or rapid transit train, often most economically by a combination of these vehicles. Excluding the motor bus, all are electrically operated. However, the fact cannot be overlooked that the motor bus has made its place in urban transit, often threatening the supremacy of the electrically operated vehicle. Looking to the future, the use of elecric vehicles should be given added impetus by the rising price of liquid fuels contrasted to a more constant price for electric power.

The Trolley Coach

The electrically propelled trolley coach is becoming very popular. It provides maneuverability in city traffic, the inherent advantages of electric propulsion, and satisfies the insatiable desire of the peoples of the world to ride on rubber tires. It will give a faster and a smoother ride than the motor bus. Wherever once installed, it has met with an instant public acclaim. For small and medium size cities, and for feeder lines in metropolitan centers, the trolley coach should have a promising future. Its rapid growth is rather significant of a return in popularity of the electrically propelled vehicle to the field of public transportation. The modern coach is not only economical to operate, but affords a fast and pleasant ride for the public upon whose patronage its ultimate success depends. As of January 1, 1948, over 6,600 trolley coaches were in service or on order.

The Street Car

The street car represents the one conspicuous application inland transportation where electricity has decreased in usage. Two principal causes have contributed to this downward trend. The motor bus, traveling a right-of-way furnished by the taxpayers, has the advantage of a very low investment, and on lines of light traffic, may operate at a low cost. The street railway industry also was slow to meet this competition with improved equipment.

To stop this trend, the operating companies and the equipment manufacturers finally combined their efforts to produce a standard surface car with excellent riding qualities that could be produced at a reasonable price. This vehicle has restored the street car to public favor by furnishing a smoother, faster and quieter ride. It has demonstrated that the street car can definitely survive for heavy surface transportation if it will keep pace with technical development.

Rapid Transit Systems

For the mass movement of passenger traffic, the rapid transit systems of large metropolitan centers have no parallel. Present four track lines can carry upwards of 200,000 passengers per hour, and the contemplated six track line for New York City should exceed 300,000 passengers per hour where over 200 trains of ten cars each may pass a given point per hour. While these systems are truly marvels of engineering, they are equally marvels of operating technique. Nowhere is reliability and safety of electric equipment required to an equal degree. Delays of a few minutes can cause a major disruption to the life of a community.

These systems represent huge investments. Thus, the major engineering considerations in their construction are directed toward moving the maximum number of people with safety and efficiency. Without electric operation, they,

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CHARLES KERR, JR. is consulting transportation engineer, Westinghouse Electric Corporation, East Pittsburgh, Pa.

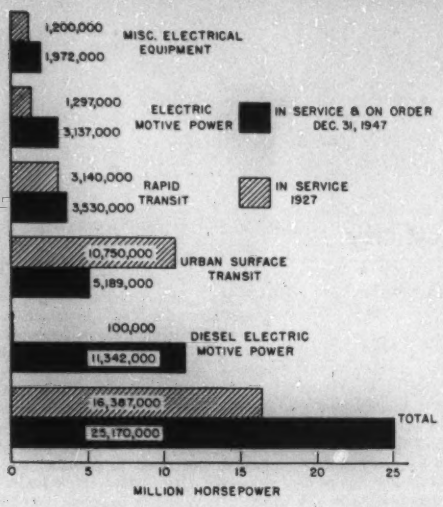


Figure 1 (left). Horsepower of electric equipment used in land transportation service

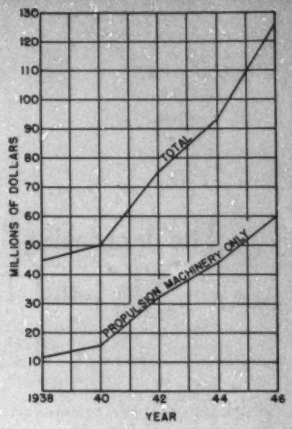


Figure 2. Annual purchases of electric equipment by the land transportation industry

and the metropolitan centers which they serve, would be an impossiblity.

Trends in Urban Transportation Equipment

Practically all city transit services operate from 600-volt d-c power. This system has proved so satisfactory that its supremacy is generally unchallenged. For the supply of this power, the mercury arc rectifier has reached universal acceptance.

But, during recent years, the apparatus used for propulsion purposes has undergone a complete transition. These changes have been dominated by a need for greater standardization to reduce first and operating costs, and by a realization that the public continually demands faster service and a better ride.

A convenient example might be taken to illustrate the trend towards standardization. Twenty years ago, the Westinghouse Company manufactured many different types of 600-volt traction motors for city transit vehicles with innumerable minor modifications of these major types to meet the particular whims of its customers. Today, three motors cover its production, one each for trolley coaches surface cars and rapid transit cars.

Accelerating rates have continually been raised until some exceed 4.0 miles per hour per second. These high rates, available only with electric propulsion, are imperative to secure fast service with frequent stops. It has been found also that fast starts are perfectly compatible with passenger comfort if the proper system of control is provided. Fast braking is equally important. All modern transit equipment has adopted electric braking, utilizing the traction motors as generators to produce retardation. By so doing, fast rates are secured, brake maintenance is reduced, and energy recovered to heat the coaches in cold weather.

Besides a faster ride, today's passenger demands a more comfortable ride. To this end, the control equipment has been developed for smooth operation. Spring supported motors and rubber insert wheels reduce noise. Forced draft ventilation is being adopted, and better lighting installed. In addition to these electrical features, the trucks and car bodies are receiving careful attention. To summarize, all modern city transit developments are being directed towards a faster and smoother ride.

Economical Use of Different Vehicles

The proper place for each type of vehicle in public transportation is governed by the volume of traffic to be carried. Figure 4 shows their respective maximum capacities in a single lane. For light density traffic, the bus can readily supply the necessary functions of public transportation, but whenever the volume reaches concentrations of appreciable magnitude, other types of transport must be utilized. This chart shows primarily the maximum capacity of these vehicles, not necessarily the most economical field for each.

Figure 5 supplies an approximate chart of their respective economical zones of application. The motor bus, due to its low initial cost and absence of large auxiliary investment, meets the needs of light traffic with ecomony. In the next zone, the electrically propelled trolley coach comes into the ascendency where sufficiently dense traffic justifies the overhead trolley investment but is insufficient to warrant the laying of track. From 4,000 to 13,500 passengers per hour, the modern surface car generally affords the most economical transportation. Whenever the density exceeds 13,500 passengers per hour, some form of rapid transit becomes essential to cope with the traffic.

This chart applies to average conditions existing throughout the United States. Among the several cities of that country or among cities of other countries, local conditions may change the exact points of demarkation between the economical zones as they are shown. However, these charts bring out one very significant fact—all large urban centers are absolutely dependent upon electrical equipment to furnish public transportation with economy.

City transit problems have not always received the logical consideration which they deserve. Fortunately, there is a growing appreciation of their importance, and especially, a realization of the rightful place for the various types of transport. These are extremely healthy conditions, and from this better understand-

ing of the problems involved, there will certainly come a better evaluation of the importance of electricity in city transit.

Suburban Operation

Suburban operations of the main line railroads are a part of the transportation service of many large cities, but are not performed by the commonly accepted local transit properties. Approximately 50 per cent of all commuting passengers in the United States are handled by electrically operated multiple unit trains.

Where congested traffic exists, the so-called multiple unit train affords a delux type of transportation for urban communities. It also removes many complicated operating problems. Where the traffic does not reach dense proportions, locomotive hauled trains furnish this service.

Recently, Diesel-electric locomotives have been introduced and some roads are contemplating a combination trolley-diesel-electric locomotive. Electric drive, whether applied to a locomotive or car, gives the rapid acceleration so essential to fast schedules.

Main Line Railroads

The uses for electricity on the main line railroads are not confined to locomotives alone, but extend to the many allied services. While motive power represents by far the largest power demands, many of the other uses for electricity are extremely interesting and represent rapidly expanding operations.

Safe high-speed train operation would be impossible without signal systems, all electrically operated. These systems have reached their highest state of perfection where a continuous indication is provided in the cab of the locomotive, making visibility of the signals entirely independent of outside weather conditions.

For communication purposes, the major railroad systems maintain huge telephone and telegraph plants. But communication between fixed wayside points alone is proving to be no longer adequate for modern operation. Today, voice communication to and from moving trains is being installed, communication between moving trains, and from head to rear end of moving trains. For this type of communication, either radio or the inductive system is used.

Communication is now going even further with passenger trains carrying telephones for the use of the traveling public. Before long, you will be just as accessible to telephone service on a train as in your own home.

There are additional applications for electric machinery in wayside operations and maintenance shops too innumerable to review. One survey indicates that 150,000 motors and generators are used for such purposes by the railroads.

The electrical equipment for modern passenger cars injects many different problems in design. The power supply for most cars is provided by an axle driven generator which must supply full load over a range in speed from 25 to 100 miles per hour with almost perfect voltage regulation. This same generator operates under the worst possible conditions and may travel from road to road, so that reliability is essential. With air conditioning and other accommodations provided by modern cars, car generating plants range from 20 to 30 kilowatts each. The peak load of a 15-car train may be 300 to 400 kilowatts, or 10 to 15 per cent of the locomotive capacity.

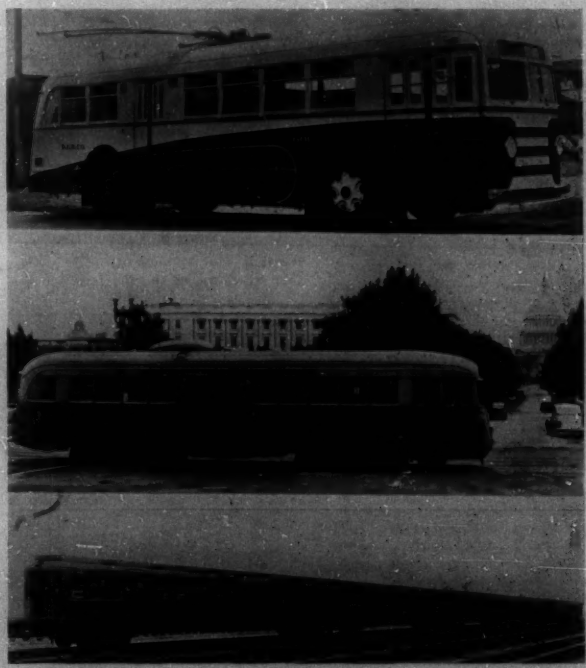
The many auxiliary services maintained by the railroads often are overshadowed by the tremendous concentration of power in the locomotives themselves. Those mentioned here are but a few of many, any one of which is well worthy of a separate paper. Modern transportation embraces uses for practically every type of electrical equipment, both in the generation as well as use of electric power.

Motive Power

Six types of locomotives will be available to move the main line traffic of the future, reciprocating steam, geared turbine, turbine-electric, diesel, gas turbine and electric. Four of these depend upon electric transmissions for traction purposes, and it is these four which are now definitely in the accendency. If this trend should continue, all main line roads eventually may be operated by straight electrification or by electric locomotives carrying their own power plants.

Figure 3. Modern city transit equipment

(Top) Trolley coach
(Center) Surface car
(Bottom) Rapid transit train



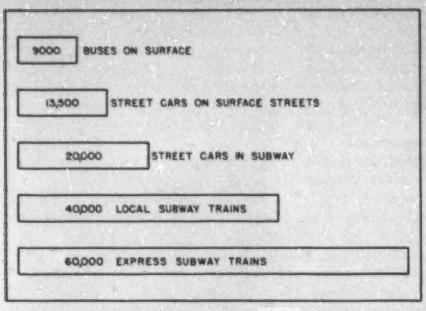


Figure 4. City transit. Capacity of a single lane of vehicles in passengers per hour

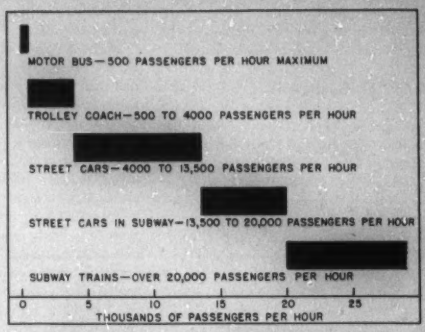


Figure 5. Economical field for application of urban transit vehicles

Electrification

Electrification is the best means yet devised for the operation of a railroad. It has only one serious limitation, a relatively large initial investment. In spite of this handicap, more railroads are adopting or expanding its use, especially outside of the United States.

Certain conceptions regarding electrification are changing, all for the good. For many years, electrical engineers fought the so-called "Battle of the Systems," or alternating versus direct current. Today, it is appreciated that either system can solve satisfactorily any electrification problem, and the final choice is dictated by local conditions. If a generalization is possible, alternating current is usually preferable where heavy concentration of loads is encountered, while direct current proves more economical where many trains are operated with smaller individual peak demands, producing a more evenly distributed system load.

It is also better appreciated that the cost and the availability of fuels are very controlling factors in the over-all economy of electrification. The prices of oil and coal are increasing while the price of electric power remains essentially stationary. (Figure 6). This relationship in the relative prices of fuel and power may exert a profound influence on the future fortunes of electrification.

The United States is blessed with a substantial supply of fuel which can be produced at a reasonable price, consequently, railroad electrification generally has been confined to those regions where traffic density dictated its use or where operating problems were encountered which could not be solved otherwise. In many other countries where fuel is not equally abundant at a low price, water power is being developed as a major source of energy, making railroad electrification an economic necessity.

As the nations of the world become more highly industrialized, rail traffic is increasing in density so that existing methods can scarcely cope with its volume. Electrification will increase the traffic capacity of these railroads at a lower first and operating cost than that involved in laying additional track. This is particularly true where the road bed is laid with light rail, the grades are severe or narrow gauge is used.

A reduction in the capital investment required for electrification would expand its application. Greater standardization of all component parts offers the best medium for accomplishing this purpose. As an example of the possibilities along this line, a complete series of locomotives from 1,875 to 7,500 horsepower has recently been proposed, constructed from identical basic components. The same four wheel trucks and the same motor would be used on all locomotives. Only the motor gearing would be changed for passenger, freight or switching service. Many control items would be identical, differing largely in quantity used according to the locomotive capacity. A series of this kind with the proper motor is equally applicable to 3,000-volt d-c systems or 12,000-volt a-c systems of electrification.

A modern design of a 7,500 horsepower locomotive, Figure 7, is illustrative of electric motive power at its best. In a single unit, greater capacity is developed than 3- or 4-unit diesels provide. All weight is carried on 16 driving axles, permitting large starting and running drawbar pull, yet imposing low axle loads on the track structure. Mechanically, it is satisfactory for speeds of 100 miles per hour or over. Weights and clearances are acceptable to any road which can handle a standard 85-foot passenger car.

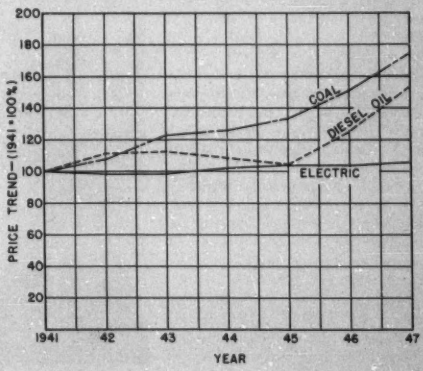


Figure 6. Trend of electric power and fuel prices for railroad service

Figure 7. Electric locomotive of 7,500 continuous horsepower

It incorporates practically every desirable feature demanded by modern motive power.

Electrification will continue to be used by the railroads of the world to a greater extent. Its rate of expansion will be determined largely by the ability of the manufacturers to reduce its first cost, by the need of the railroads to increase their traffic capacity, and by the relative price of electrical energy compared to the availability and price of other fuels.

The Diesel Electric Locomotive

Wherever Diesel electric locomotives are discussed, these advantages are always cited:

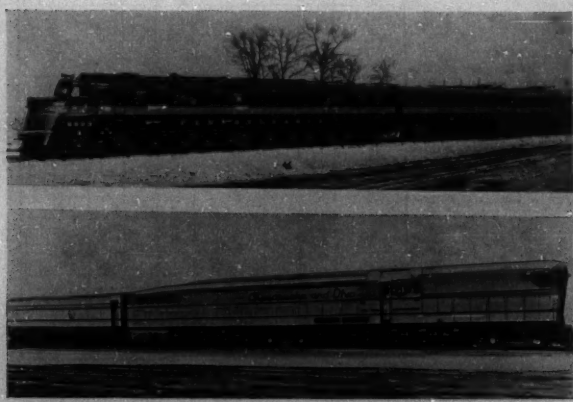
1. Constant rail horsepower over the normal operating range of the locomotive, providing a versatility unobtainable from the reciprocating steam locomotive.
2. A multiplicity of lightly loaded driving axles with all or most of the locomotive weight utilized for adhesive purposes.
3. Exceptionally high starting and running draw bar pull.
4. A decided increase in train tonnage.
5. Ease in the handling of heavy trains.
6. Electric braking on long descending grades.
7. Elimination of helper locomotives over ruling grades.

These desirable locomotive features are produced by the electric transmission. The same characteristics could be obtained with electric transmission from any other type of power plant. It is not the purpose of this comparison to decry the many important contributions to railway operation which have been made by the Diesel engine, but to point out the significant contribution of the electrical transmission which is not too universally appreciated or understood.

Figure 9 shows why electric transmission is so important. One curve on this chart shows the ideal speed-tractive effort of a 4,000 horsepower locomotive. This ideal curve represents a constant horsepower at all locomotive speeds, a characteristic which would secure full utilization of the locomotive's inherent capacity under every operating condition. No existing prime mover will meet this performance with direct mechanical drive. The reciprocating steam engine most closely approaches it, which accounts for its long survival in land transportation. Also shown by Figure 9 is the typical performance of a Diesel engine with any form of direct mechanical transmission. This performance in itself is not suited to traction purposes, but through the use of the electric transmission can be converted into the ideal performance except for that loss in efficiency encountered in the drive apparatus.

Figure 8. Modern main line locomotives

(Top) Diesel, 6,000 horsepower
(Bottom) Turbine electric, 6,000 horsepower



The Diesel electric has no equal for train switching service. Locomotives of 660–1,000 horsepower replace steam power of considerably greater capacity. These locomotives operate for long periods without attention. Their refueling is simple, their maintenance expense is low, their efficiency is high, and their standby losses are nil. With such favorable operating expenses, they promise to replace all other types in switching service. Diesels are also rapidly extending their operation to road service. In these applications, the locomotives range from 1,500 to 6,000 horsepower composed of one or more units of 1,500, 2,000, or 3,000 horsepower capacity. The average size of all road locomotives is between 4,000 and 4,500 horsepower.

The phenomenal increase in the use of diesel motive power is illustrated by Figure 10. Those few railroads with electrifications have long appreciated the electric transmission and the advantages which it provides. The Diesel electric locomotive is extending these advantages on a world wide scale.

Steam Turbine-Electric Locomotive

The coal-burning steam locomotive has now adopted electric drive. In 1947, the Chesapeake and Ohio received the first coal-burning steam turbine locomotives having a rated capacity of 6,000 horsepower. These locomotives will haul high speed, streamlined passenger trains on exceedingly exacting schedules.

The locomotive arrangement itself is quite novel. Coal is carried at the front end, followed by the operator's compartment. The boiler is next in line, and the turbine-generator plant at the rear. A separate tender carries water. Eight axles are equipped with traction motors.

In 1944, the Pennsylvania Railroad introduced the first geared turbine locomotive of large capacity. Experience with its operation has shown that a turbine will operate reliably and at low maintenance in locomotive service. With electric drive, the turbine should give equally satisfactory performance.

In many countries, coal is the one assured, long range source of fuel. Improvement in the coal-burning locomotive to make it thoroughly competitive with the Diesel will be welcomed by roads so

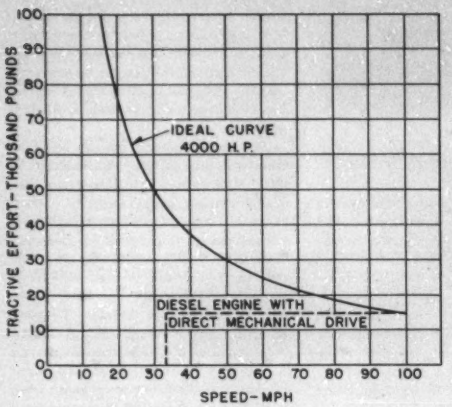


Figure 9. Ideal 4,000 horsepower locomotive performance versus Diesel engine performance

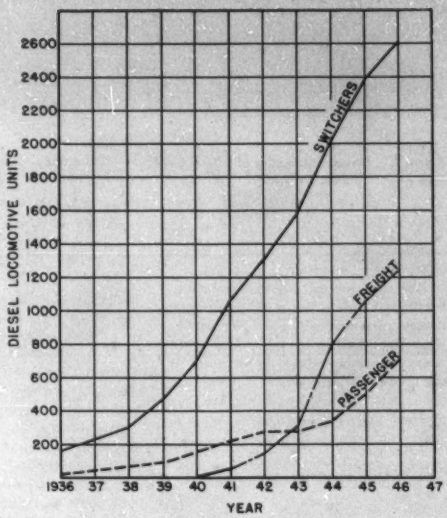


Figure 10. Diesel locomotive units operated by class 1 railroads

situated. The Chesapeake and Ohio locomotives will be watched with interest by railroad men everywhere.

A more efficient utilization of coal could be secured by the adoption of a high-pressure high-temperature boiler. Extensive work is now under way on this problem and if it is satisfactorily solved, the coal-burning locomotive could easily return to popular favor. The high-pressure steam locomotive, when it appears, will undoubtedly utilize a turbine-electric drive, thus, a return to favor of the steam locomotive should further increase the use of electric equipment by the railroads.

The Gas Turbine Locomotive

From the long range viewpoint, the possibilities of the gas turbine locomotive are most attractive, including:

1. Elimination of the boiler, substituting in its place a means to utilize the products of combustion directly in the prime mover.

2. Elimination of all reciprocating machinery, substituting therefore purely rotating apparatus which, when once perfected, normally operates at low-maintenance expense and for long periods without attention.

3. A prime mover with excellent weight and space characteristics which should permit locomotives of large capacity to be built in one unit.

4. For the first time, a means becomes available to burn low cost fuels in locomotive service at high efficiency.

The gas turbine locomotive is coming. One manufacturer has had a 2,000 horsepower turbine generator set operating on the test floor for almost two years. Bituminous Coal Research is developing two coal-burning gas turbine locomotives. Several turbine manufacturers have plans to equip locomotives in the near future.

Probably the first gas turbine locomotives will be oil fired. Later models will burn coal. This sequence will be dictated by the greater ease with which liquid fuels can be satisfactorily burned.

The gas turbine eventually will prove to be a strong competitor of all other types. It has so many potential advantages that its place in the transportation picture cannot be denied. But, before proved commercial units are available, extensive development will be required. At first, fuel economy will not be as good as the Diesel, but that will be compensated

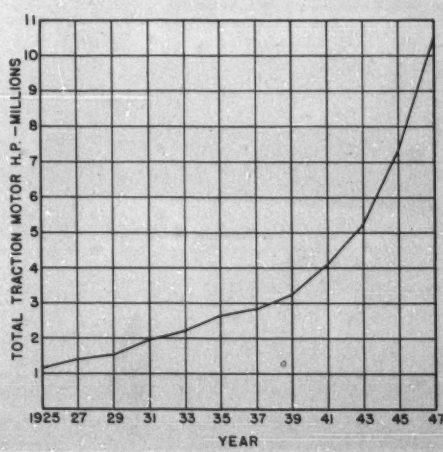


Figure 11. Horsepower of traction motors operated by class 1 railroads

by lower maintenance, and will improve with further development.

Electric drive is equally advantageous to the gas turbine locomotive. When the simple open-cycle gas turbine is used, which seems to be preferred in locomotive design, this transmission is a necessity because of the inherent speed-torque characteristics of this type unit. This new type of motive power also will extend, not restrict, the trend of the railroads towards the movement of traffic with electric drives.

Trends in Motive Power

Electrical engineers have dreamed for many years of the day when the railroads might be electrified. Unless something halts the present trend, that day may come, with electrification consisting partly of electric locomotives receiving their power from a central power station, and predominantly from electric locomotives carrying their own power plants.

Figure 11 shows the total horsepower of traction motors used by the railroads of the United States. Until 1936, this curve represents primarily the extension of electrification. After 1936, it represents electrification and Diesel electric locomotives. In 10 years, the total capacity of the diesel locomotive motors has greatly exceeded that of electrification, and by the end of 1948, approximately another 3,000,000 horsepower will be added. Should all locomotives eventually be converted to electric drives, the installed capacity of electric motors on the railroads would exceed 125,000,000 horsepower, or almost twice the installed capacity of all central power stations.

Literally, a revolution is taking place in the motive power field. An interesting fact is the importance of the electric drive in this change. Its advantages have been so well demonstrated and accepted by the railroads that its position seems invulnerable to the possible vicissitudes in the fortunes of the various prime movers. Whether the railroads electrify or use steam, Diesel or gas turbine prime movers, their motive power will be propelled by electric traction motors to a rapidly increasing degree.

Conclusion

A brief summary of the uses for electricity by land transportation services has been presented for the United States. It would have been even more desirable to present similar figures for the railroads of the world, but unfortunately, accurate data on a world-wide basis are unattainable. It was felt preferable, therefore, to give an adequate picture as it applied to one country rather than a more universal picture which, of necessity, would be both inaccurate and inadequate.

The land transportation industry, with an installed capacity of 175,000,000 horsepower, constitutes the nation's greatest user of power. In the not too distant future, with an uninterrupted continuation of present trends, this industry also will become the nation's recognized leader in the use of electric equipment.

No Discussion

Standardization in the United States

H. S. OSBORNE
FELLOW AIEE

Synopsis: Standardization in Mexico extends back over a considerable number of years. In 1926 the Secretariat of Industry and Commerce prepared a National Electrical Code which has been influential in guiding Mexican progress ever since. In recent years, standardization in a broad field has been an important activity of the Ministry of National Economy which in 1943 established the Direccion General de Normas, (DGN). Under the initiation of DGN and with the co-operation of Mexican industry and professional organizations more than 100 standards have been adopted to guide Mexican federal government purchases, covering a wide range of commodities.

Scope of this Discussion

THE term standardization may be applied to very broad and diverse fields of activity. We will consider primarily the development of technical standards, namely, those types of standards of most interest to the AIEE and other electrical engineering societies. As a general background, however, this paper refers to the activities of the American Standards Association and of the organizations associated with it, activities which cover a far broader field.

In the field of electrical standards the AIEE has long been a leader. The first standards for electric machinery generally accepted in the United States were produced by the AIEE in 1899, and the Institute has a record of continuous activity in producing standards in the electrical field since that time. Recognizing the growing importance of standards and particularly the importance of a generally accepted means of establishing the national recognition of standards, the AIEE was one of the small group of organizations which formed the American Engineering Standards Committee in 1918. It has subsequently helped that committee and its successor, the American Standards Association (ASA) to develop to meet the increasing responsibilities of a rapidly growing national standardization movement.

Purpose of a Standard

Basic to the methods and procedures used in the United States is the fact that to receive national acceptance, standards must be designed to serve the interests of the nation as a whole. Similarly, international standards must be designed to be helpful to interested groups throughout the world.

The procedures to be followed in establishing a standard naturally partake of the nature of the society which they serve. In a democratic society all standards to be effective must represent a general consensus of expression of those most concerned. This applies even to those types of standards which are incorporated in government regulations. It applies with much greater force to the standards which in the free economy of the United States constitute the great bulk of the work of standardizing groups, namely, voluntary standards developed by those most concerned and depending upon their voluntary acceptance for their use.

The subject matter of a standard may be methods of measurement, terminology, dimensions, and other physical characteristics, operating practices, provisions for safety or any other of a very wide range. Whatever the subject matter, the standard is an expression of the current view of those best informed on that subject as to what is most desirable for the advancement of the industry and the elevation of living standards of the people. Where there are differences in opinion among those representing different groups, the standard must represent a conscientious effort to find the solution rather than merely a compromise between conflicting claims. Furthermore, such a standard is dynamic, not static, and it should be modified promptly as the result of further information or changes in conditions.

Methods

The ASA grew out of recognition of a need for a formalized procedure to determine when a standard meets the outlined requirements and an organization to certify to this fact. The basic control of the Association is in the hands of its member bodies; 64 nationwide associations and groups of associations widely interested in standardization work. These member bodies include 14 professional societies, 40 trade organizations, and 10 departments of the federal government. In addition, more than 2,000 companies and individuals are associated in this work as members in various classifications.

The basic principle of operation of the ASA in the approval of a standard as an American Standard, is that all groups vitally concerned in a given standard, must have been consulted and given an opportunity to express their views, and that the standard itself must represent a consensus of these interested groups. In the practical application of this principle of consensus the work must be broken up into sufficiently small parts so that all interested in each part can participate effectively.

It has not been found possible to draw a hard and fast line as to the degree of unanimity necessary to justify the certification of a standard as an American Standard. All those concerned must

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H. S. Osborne is chief engineer, American Telephone and Telegraph Company, New York, N. Y.

have been given full opportunity to express their views, and where there are individual objections these must have been fully considered by all of the group. If, after weighing these objections, they have been rejected by all but a very small minority, it is considered that the consensus is established. In determining whether a minority is sufficiently small to be overruled it is necessary to take into account the nature and extent of the interest represented by the dissenting votes as well as their number.

Procedure

Under the broad principle that a consensus must be established, the procedures of the ASA recognize a wide variety of methods by which the interested groups formulate standards which are to be submitted for approval as American Standards. Any method which meets the broad requirements is acceptable.

The procedure which is most widely used is the formation of a sectional committee under one of the standards-making organizations as sponsor. The sponsor organizes the committee, which is approved by the ASA as adequately representing all interested groups, consumers, producers, government departments, distributors, insurance companies or others, depending upon the nature of the subject matter. When this committee has completed its work and the resulting standard is approved by the sponsor, the evidence as to the degree of unanimity is submitted to the ASA with the request that the standard be approved as an American standard.

In the electrical field, standards for electric machinery, which were first produced by the AIEE, also are of very direct interest to other associations representing manufacturers and the power companies. Before the organization of the ASA, competition had grown up between these organizations in the preparation of standards. It was therefore arranged that as a part of the ASA there would be an Electrical Standards Committee in which all of these groups were represented. The Electrical Standards Committee recommends to the ASA the sponsorship for each case and in some cases assumes sponsorship itself where this is considered desirable, rather than having the work carried on by a single organization.

Similar correlating committees have been developed in other fields of work, providing expert agencies of the ASA in these fields. The fields represented at present are mechanical, safety codes, building codes, mining and consumer goods.

Relations Between Industry and Government in Standardizing Work

In all of this work, government departments and bureaus participate on the same basis as any other organized technical group or industrial group. Participation in the development of each standard is open to all those who have an important interest. Those who can contribute information or work are urged to take an active part. At the present time 50 government bureaus are officially represented on committees responsible for 350 standards.

International Work

As the ASA is the focal point of national standardizing work, and its members include a large number of organizations most active in and concerned with the development of standards, it is an ideal association to represent the United States in the development of international standards. The ASA was active in the recent formation of the International Standards Organization (ISO) and H. Coonley, Chairman of the Executive Committee of the ASA, was elected the first President of the new international organization. Mexico is also a member of the ISO.

For many years, the International Electrotechnical Commission (IEC) has been the authoritative body for carrying out international standardization in the electrical field. The same electrical organizations which carry out the national standardization work in the electrical field in the United States, by cooperation through the procedures of the American Standards Association, have also for many years been the principal contributors, both in work and in money, of the United States National Committee of the IEC. Accordingly, it was arranged that this committee should become an integral part of the ASA. This desirable integration of acitivities took place in 1930 and has proved advantageous. It is now anticipated that a similar integration will take place between the IEC and the new International Standards Organization, the IEC remaining, however, the authoritative vehicle for international standards in the electrical field.

Conclusion

All of this work represents a growing appreciation in the United States, as in many other countries, of the importance of standards both in improving the economic condition of the country and also in helping to stimulate international trade and improve economic conditions generally throughout the world. Important ways in which standards are helpful include the following:

1. To create understanding and avoid disputes. For example, a purchaser can use standards as a basis of stating exactly what he is ordering and determining whether or not he receives what he ordered.

2. Improve working conditions. Numerous important safety standards emphasize this point.

3. Assist in the economy of production. Standard dimensions, methods, and performance characteristics all tend in this direction.

4. Increase convenience of use. Interchangeable parts and standards of quality illustrate this point.

5. Reduce trade barriers within a country and to the extent that they are used by many countries internationally.

6. Important for defense. This applies not only nationally but internationally to standards used in common by groups of countries who may have occasion, as in the two recent World Wars, to join together to defend themselves against an aggressor.

The ASA has given special emphasis to developing the closest possible ties in standards work between the United States and the other republics in the western hemisphere. With this in view, the Association has had for several years a Latin American department. The purpose of this department is to stimulate rapid interchange of information on the progress of standards work in the various countries, arrange for translations of standards from English into Spanish and Portuguese and vice versa, and always to stimulate the closest co-operation between the standards-making bodies in the various countries. The importance of standardization is widely recognized in the countries of North and South America and is indicated by the fact that seven of these countries have national standardizing bodies Argentina, Brazil, Canada, Chile, Mexico, United States, and Uruguay.

Co-operation between Mexico and the United States was discussed at the Mexican-American Conference on Industrial Research held in Chicago in October, 1945. I hope that this discussion which may in a sense be considered a return mission to Mexico, will further stimulate co-operation in standards work in the two countries, adding one more tie to those which closely bind together these two friendly neighbors.

No Discussion

Trends in Electrification of American Steel Industry

L. A. UMANSKY
FELLOW AIEE

THE STEEL INDUSTRY in the United States is largely electrified. It is one of the largest users of electric power. For instance, in 1947 this industry has produced close to 85,000,000 net tons* of steel and has consumed approximately 200 kilowatt-hours per ton, or a total of about 17,000,000,000 kilowatt-hours. Only one other group of industries, the electrochemical, rivals steel as user of electricity.

This well established partnership of electricity and steel is not a mere coincidence. As will be shown later, it is based on sound engineering and economic factors. Nor is their partnership new; 25 years ago close to 90 per cent of power applications in the steel industry were motorized. Yet, during these 25 years, the type of electrical equipment used in the industry has radically changed. This equipment became more intricately connected with the steel manufacturing processes. More than that, the processes themselves had been redesigned around the electrical equipment, in order to make full use of its advantages.

This paper attempts to give a brief review of these changes and to point out the role of electricity in improving the processes and in lowering the production costs.

Generation and Purchase of Electric Power

Basic Facts

An integrated steel plant starts with iron ore, coal, and limestone as the principal raw materials, and ends up with finished rolled-steel products. The main component parts of such plant are coke ovens to convert coal to coke and by-products, blast furnaces to convert iron ore to pig iron, open hearth furnaces to produce steel out of pig iron and scrap, and rolling mills to give the steel the needed shape and form.

The natural by-product of a blast furnace is gas. While it is not very high in heat content (about 90 to 100 Btu per cubic foot), it is produced in such large volume that a sizable portion of it is usually available for power generation. On the other hand, the rolling mills are large users of power. They are usually located some distance away from the blast furnaces. Thus, a power house near the furnaces, electric transmission of power to the mills, and electric mill drives is the logical and accepted arrangement.

To better understand the relation between the heat balance of a steel plant and the generation of power, the following figures should be helpful.

To produce a ton of pig iron the industry has used an average of 1.725 tons of iron ore, 0.934 ton of coke, 0.41 ton of limestone, and, last but not least, 4.5 tons of air. In 1947 the United States blast furnaces have produced about 58,500,000 tons of pig iron; about 10 per cent of this amount being used as "merchant iron" in foundries. Thus, some 52,500,000 tons of iron were left for converting into steel in open hearth and electric furnaces and in Bessemer converters. Since 85,000,000 tons of steel were produced in the same year, the pig iron contributed only 62 per cent of this amount; the balance, or 38 per cent, was made up of scrap.

The 85,000,000 tons were the weight of steel ingots as poured from melting furnaces. By the time this steel has been rolled, trimmed and sheared, the weight of the finished products, shipped from the steel mills, became only slightly over 63,000,000 tons. The "loss", or 22,000,000 tons, constituted part of the scrap needed for feeding the melting furnaces.

Now these figures are the average of the whole steel industry. They may not be true for all, or for any, specific mills. This fact should be firmly kept in mind. The "average" steel plant which will be presently discussed is merely a hypothetical plant.

Waste Gas as Source of Power

Let us assume that this plant produces 0.6 ton of pig iron for every ton of steel ingots. The corresponding amount of blast furnace gas is about 70,000 cubic feet. Not the entire volume of gas is available for power generation; about 50 per cent is used for serving the blast furnace itself; for pre-heating the air blast required for furnace operation, and for generating steam to drive the turbo-blowers. The remaining 50 per cent of gas could be used for power generation, if there were no other equally legitimate demands for this gas around the steel plant.

The 50 per cent of 70,000 cubic feet, at 95 Btu per cubic feet, will give us 3,330,000 Btu. Allowing 17,000 Btu per kilowatt-hour, generated in a steel mill power house, we could produce about 195 kilowatt-hours per ton of steel ingots.

Theoretically, this could just suffice for operating the steel plant. Actually, the amount of generated power will fall short of this amount. The present tendency is to give priority to the use of gas for heating purposes, at the soaking pits, coke ovens and reheating furnaces. This reduces the share of gas available for power generation. Furthermore, it is not possible to rely on the "average" figures as we have done in the foregoing ex-

*Throughout this paper "ton" refers to the "net" or "short" ton of 2,000 pounds. The "long ton" used previously is 2,240 pounds, while the "metric ton" equals 1,000 kilograms or 2,205 pounds.

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L. A. Umansky is with the industrial engineering divisions of the General Electric Company, Schenectady, N. Y.

Figure 1. 12,500-kw Scherbius-type frequency changer installed in a steel mill for interconnecting the 25 and 60-cycle power systems

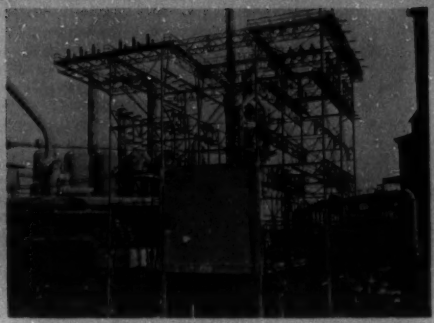

Figure 2. Electronic frequency changer in a steel mill for interconnecting the 69-kv, 60-cycle, and 44-kv, 25-cycle systems

Two 10,000-kw units installed in brick-walled section of the steel tower

ample. Many power consuming departments, such as rolling mills, are ordinarily not working during week ends, while the blast furnaces are on a seven-day schedule. The ratio of pig iron to steel varies from plant to plant. The total power consumption per ton of steel also varies, depending on the kind of finished product.

Some integrated steel plants, complete with blast furnaces, produce heavy steel sections such as rails, structural shapes or large merchant bars. These plants require considerably less than 200 kilowatt-hours per ton of ingots, and plants of this type may be more nearly self-contained in generating their own electric power. Conversely, other plants, producing smaller sections and particularly light flat products, like sheets and tinplate, require more additional power. Since the proportion of these light sections in the American steel industry is growing, the need of additional power, generated from coal or purchased, also goes up.

The long range trend in the steel industry as a whole is in favor of purchasing this additional power rather than generating it from burning coal. The rising cost of coal and labor, the continuing low cost of purchased power, the desire of steel companies to apply their investment capital for steel making and processing facilities, readily explain this trend.

In addition to the blast furnace gas, a steel plant has a few other, less important, sources of by-product fuel or heat, as well as other demands for them. However, their presence does not change the general picture. Coke breeze is usually burned under boilers as a supplementary fuel to blast furnace gas; waste heat boilers in open hearth furnaces can produce some additional steam; coke oven gas and tar, being too valuable, are not burned under boilers. Some steam is needed for miscellaneous processes and for heating of buildings.

Some 25 years ago the American steel industry, taken as a whole, generated approximately two thirds of all electric power which is consumed. At present this proportion has dropped to about 50 per cent. In spite of this trend many steel mills have power plants well in excess of 100,000 kilowatts installed, and these plants are growing. Turbogenerators of 30,000, 40,000 and 50,000 kw are encountered, although units of 20,000–25,000 kw are more representative. A number of gas engine generators, operating on blast furnace gas, are still in service, but it is questionable whether many units of this type will be installed in the future.

Character of Steel Mill Load

From the viewpoint of power supply, whether from its own power house or a public utility connection, the steel mill load has good characteristics. For example, the ratio of peaks to average demand is not excessive. The following example may serve as a rough guide.

Assume an integrated steel plant rated to produce 1,000,000 tons of steel ingots annually and operating at capacity. Allowing 200 kilowatt-hours per ton, the total power consumption should be close to 200,000,000 kilowatt-hours per year. If spread over 8,760 hours, the average load would be 23,000 kw. If week ends and annual holidays are not counted, leaving us about 7,000 hours, the true average during the working period will be some 25 per cent higher, or 28,500 kw. Of course, the load is not constant even during this period; we can expect the maximum 1-hour demand to be some 20–25 per cent higher than the working day average, or about 35,000 kw. Finally, we should encounter momentary peak loads of about 150 per cent of the working day average, or about 43,000–45,000 kw. The power house or the tie with the power company should be designed accordingly.

Quite obviously, when the steel plant operates below its rated capacity, the average load goes down rapidly while the maximum demand is apt to remain high.

Frequency

Many large steel plants built in the period 1900–1920, when electrification was making its first strides, have selected 25 cycle as the preferred frequency. Since most of these plants were of the integrated type, with their own power houses, and since ties with utility systems were not considered then as important as they are now, the steel industry had a free hand in making this choice.

The 25-cycle system was chosen primarily from the viewpoint of rolling mill drives. Induction motors were almost universally used. Low-speed motors for direct drive were preferred since reduction gears were not considered then as sufficiently reliable. These factors obviously favored the lower frequency than the conventional 60 cycle.

On the other hand, many smaller steel mills without power houses of their own and relying on purchased power had to adopt even then the normal frequency of 60 cycle. Quite a few of them grew to be large plants since that time, but of course carried on with that frequency.

Under present conditions the use of 25 cycle offers little advantage. The contrary rather is true. The design of gears

has reached a high degree of perfection and reliability. In many cases, even when low-speed motors are employed, the synchronous motors can be successfully applied, and these are not penalized by the combination of low speed and normal frequency. Furthermore, fewer and fewer a-c drives are used, their place being taken by d-c machines, as we will see later. A synchronous motor-generator to supply power to d-c machine can be built as well or better for 60 cycle as for 25 cycle.

The 60-cycle turbines, motors and transformers cost less than their 25-cycle counterparts. Being standard in the industry they are more readily available. Higher motor speeds, such as 3,600 and 1,800 rpm, are possible as against the maximum 25-cycle speed of 1,500 rpm. Greater choice of speeds is offered. Many pieces of machinery, as for instance pumps, fans, and machine tools designed for general use with 60-cycle motors are made available for steel mills adopting this frequency.

It is not surprising, therefore, that the 60-cycle frequency is rapidly gaining ascendancy in the steel industry. No new plant built in the last 20–25 years employs 25 cycle, except of course for some extensions of the older plants. Many other mills have ceased adding to their 25-cycle systems, and are using 60-cycle power for their new additions. If 25 years ago the ratio of 25 to 60 cycle capacity installed in the steel industry was 2 to 1 or more, this ratio is now less than 1 to 2 and is rapidly growing smaller.

This change entails the use of frequency changers for tying together the 25- and 60-cycle systems. Ordinarily, the steel plant continues to supply 25-cycle power to its older mills. New additions are connected to either purchased 60-cycle power or to the new 60-cycle generators of the plant. In either case, the interchange of power between the two systems is essential in order to increase the effective reserve capacity at the power house, and to have greater flexibility in operating the newer and more efficient generating units.

Several types of frequency changers may be considered. The simplest and the least expensive is the synchronous-synchronous unit. It permits of flow of power in either direction and is also suitable for stub-end feed operation. However, the 25-cycle systems do not maintain their frequency as well as the newer and larger 60-cycle systems. Variations from 24 to 26 cycle are encountered. This imposes severe duty on the frequency changer. Experience dictates that the capacity of the latter should be not less than 25 per cent of the generating capacity on the smaller of the two systems. The control of the flow of power has to be done by the governors on the 25-cycle turbogenerators. A more flexible frequency changer is of the Scherbius type. The 60-cycle unit is a synchronous machine connected to a 25-cycle slip ring induction motor. The latter has its rotor circuit connected to an a-c regulating machine of the commutator type. By suitably controlling this machine, it is possible to control the magnitude and direction of the flow of power through the set, regardless of variations of supply frequency. It is also possible to supply power to either system in the stub-end fashion, without any synchronous machine being then connected to the system. A 12,500-kw unit of this kind is in successful operation in one large steel plant near Pittsburgh, Pa.[1]

Last, but not least, is the electronic frequency changer. This equipment consists essentially of two rectifiers connected through transformers to the respective power systems. The d-c ends of the two units are linked together. At any given time one unit acts as an a-c to d-c rectifier, while the other as a d-c to a-c inverter. The flow of power is controlled by phase control. This equipment is also of the asynchronous type, that is, the flow of power can be controlled independently of frequency variations on either system. The flow of power may be reversed at will, but the converter must receive reactive kilovars from both systems. Thus, it is not readily suitable as a source of stub-end power supply. Two 10,000-kw frequency changers of this type are in successful operation for about six years in a large steel plant near Pittsburgh while another unit of 6,700-kw capacity is operating for about the same length of time near Chicago, Ill. Being a static device, the electronic frequency changer does not require heavy foundations. As shown, the two 10,000 kw units are mounted in a steel tower, tying the 69-kv, 60-cycle, and 44-kv, 25-cycle transmission lines.[2]

The chief drawback to the wider use of this type of equipment is the drain of kilovars from the systems and its unsuitability for stub-end operation.

System Voltages

Two voltages were always popular and widely used in the steel industry: 2,300 and 6,600 volts. The former voltage originated in smaller plants which were not expected to grow larger, but often did in defiance of these expectations. Quite obviously, the 2,300-volt system could not very well keep pace with the growth of the plant. The larger steel plants have chosen, as early as 40 years ago, 6,900/6,600 volts as their voltage.* This volt-

*When speaking of generator or system voltage, it is correct to use the values of 13.8–11.5–6.9–2.4 kv and 480 volts. The corresponding voltage ratings of motors and other power utilization equipment are 13.2–11.0–6.6–2.3 kv and 440 volts.

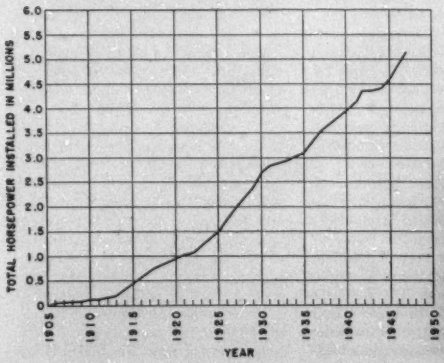

Figure 3. (left). Cumulative curve of rolling mill drives

Figure 4. 10,000-horsepower twin-motor drive for a large reversing slabbing mill

Each roll is driven by a separate 5,000-horsepower 40/80 rpm motor

age is still by far the most popular in the steel industry, and is used frequently for power generation and distribution, as well as for large motor drives. As the plants and systems grow, the adequacy of this voltage for all three purposes becomes questionable.

Turbogenerators of 20,000–30,000 kw (not to speak of larger units) lend themselves better to a voltage of 13.8 kv rather than 6.9 kv. Bus runs from the generators to the switchgear and the switchgear itself are definitely less expensive at the higher voltage.

Considering the available standard and well developed apparatus, it is highly desirable to limit the circuit breakers at the mill substations and for large motor drives to the 500,000-kva rating available in the metal-clad form, both for 6,900 volts and higher voltages. However, on large 6,900-volt systems, the factor limiting the circuit breaker selection is more often than not, the momentary asymmetrical fault current, rather than the interrupting capacity. To cope with this limitation a greater sectionalization of circuits or an excessive size of current limiting reactors become essential. At voltages like 11.5 or 13.8 kv, these problems seldom occur. Better circuit arrangement and better switchgear selection is made possible. It is, of course, obvious that the cost of feeders is lower with the higher voltage.

As far as large motors are concerned, the use of 6.6-kv voltage is satisfactory from about 1,000 horsepower and up to the largest size ordinarily encountered in steel mills. For small and medium size motors 440 volts is usually supplied. There remains a twilight zone, 500–1000 horsepower, where 440 volts is low, while 6,600 volts is to high for best all-around results. Compromises are usually made for this range of motor sizes since very seldom an intermediate voltage like 2,300 volts is introduced between 6,600 and 440 volts.

However, when 13.8 or 11.5 kv is used for distribution, an intermediate voltage (2.4/2.3 kv) becomes practically mandatory and a more versatile "3-voltage-standard" is established for the plant. Under this standard the large motors (synchronous 2,000–2,500 horsepower and larger, induction 3,000–3,500 horsepower and larger) can be chosen for 11-kv or 13.2-kv operation, medium size motors down to about 200 horsepower can be built for 2,300 volts and for the smaller motors the low voltage of 440 volts can be used of course. For supplying the 2,300-volt and 440-volt power, localized distribution systems are then provided, energized from load-center step-down transformers. These low-voltage systems, being local, do not extend over the entire plant as the main high-voltage distribution system does.

For a large new steel mill, of say 1,000,000–2,000,000 ton annual capacity, with generating capacity up to about 75,000 kw (or equivalent tie-line with public utility), a 3-voltage power system of 13,800–2,400–480 volts can be well recommended.

Several steel mills, which have originally started with a 6,900-volt system, have now grown to such large size that the power demand exceeds 100,000 kw. Considering the already made large investment in 6,900-volt equipment, it is sensible for these large plants to leave most of this equipment intact (if otherwise adequate), but to replace the 6,900-volt distribution system with a higher voltage, such as 24 kv, 34.5 kv, or even 69 kv, and to provide several major substations stepping down from this high voltage to 6,900 volts. This means, in effect, splitting the plant in electrical sense into several smaller units, tied together by the high-voltage "backbone."

Several large steel mills have already adopted this system of modernization.

Rolling Mill Drives

The major portion of power supplied to a steel plant is consumed by the rolling mill drives; that is by the work of shaping and otherwise processing the steel from ingots to finished rolled products, such as sheets, bars, rails, structural shapes, and rods. Electricity plays a predominant part in this work.

Even 20 years ago more than 90 per cent of the rolling mills were electrified. Since 1929 to 1947 the installed capacity of the drives has increased about 250 per cent, while the ingot production went up only 50 per cent. Several reasons explain this fact.

In the first place, as already noted, the industry now converts a greater portion of its tonnage into light flat rolled sections, such as sheets and strip, requiring greater power for rolling. In 1925 these products comprised 17 per cent of the total finished steel, in 1947 this percentage increased to 28.9. Secondly, many rolling methods have radically changed, calling for vastly

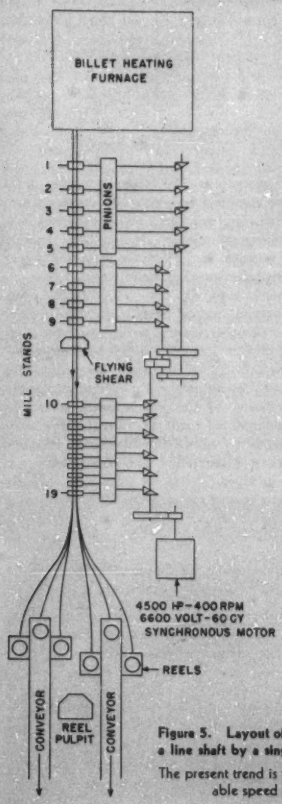

Figure 5. Layout of a rod mill driven through a line shaft by a single constant speed motor

The present trend is to provide several adjustable speed drives (Figure 9)

Figure 6. 4,500-horsepower, 400-rpm synchronous motor driving the mill shown on Figure 5

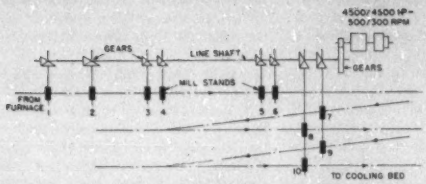

Figure 7. Layout of a merchant mill, designed 25 years ago and driven by a single adjustable speed motor

Figure 8. 6,000-horsepower Kraemer drive for a large merchant mill installed in 1929

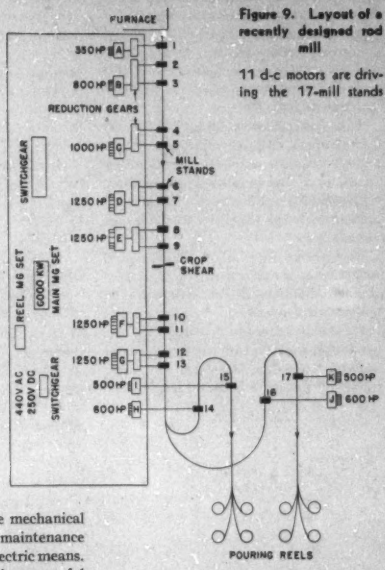

Figure 9. Layout of a recently designed rod mill

11 d-c motors are driving the 17-mill stands

differing mills and for more powerful and numerous drives.

A brief review of these trends is given in the following.

Reversing Drives

The principal application of these drives is to blooming and slabbing mills, which roll reheated ingots into such semifinished products as blooms and slabs. Reversing plate mills, rail mills and the like also fall in this category.

In general, one may conclude that these types of drives have maintained their principal features. The mill is driven by a low speed, directly coupled d-c motor or motors, supplied with power from one or more d-c generators. The latter are driven either by an induction motor equipped with a heavy flywheel, or in some cases by a synchronous motor Field control of the d-c machines provides means of reversing the drive and of adjusting its speed.

A closer analysis of many of the late installations will show that new and fruitful ideas were introduced and put in practice in the last quarter of the century.

The so-called twin drive should be mentioned first.[2] With this arrangement the top and bottom rolls of the mill are driven by a separate motor; nothing but the hot metal ties the two rolls (and the two motors) together; yet the electrical control keeps the two motors in step and successfully divides their load evenly. The pinion stand (the 1-to-1 gear unit conventionally tying together the rolls in the mill) is eliminated; thus the mechanical equipment is simplified and maintenance is reduced by a wider use of electric means. Seven such equipments are in successful operation in the United States, the largest being rated 10,000 horsepower at 40/80 rpm. This number does not include cold strip mill drives, of which more will be said later. A still larger drive has been provided for a reversing roughing stand of a plate mill, rated 7,000 horsepower, 25/50 rpm, which is capable of developing a maximum torque of approximately 4,000,000 pound-feet (550 ton-meters), which is believed to be the highest value of torque of any mill drive.

The most noteworthy development in the control of these mills has been the wide use of rotating regulators and amplifiers (such as amplidyne). Separate exciters are usually provided for the reversing motors and for the corresponding generators. The fields of these exciters are under control of several amplidynes arranged to get these principal results:

1. Extremely quick acceleration and reversal of the mill. (For instance, a 10,000 horsepower, 40/80 rpm drive reverses from plus 40 rpm to minus 40 rpm in less than $1^1/_4$ seconds.)

2. Effective limitation of current peaks beyond the capacity of the machine.

3. Maintenance of mill speed regardless of flywheel speed regulation.

4. Great simplification of control apparatus.

The use of synchronous motor-generators instead of flywheel sets has made some gains. The largest reversing drive so equipped is a 6,000 horsepower structural mill with an 7,500 horsepower synchronous motor driving the motor-generator set. With the steel mill power systems growing rapidly, an occasional peak load of, say, 250–300 per cent normal can be readily absorbed, while taking full advantage of lower first cost, lower maintenance, and leading power factor of the synchronous versus flywheel set. By automatically controlling the excitation of the synchronous motor as function of load, the effect of the peak loads on the system voltage is reduced to a minimum. This has been very effectively done, for instance, through the use of an amplidyne power factor regulator for a 4,000 horsepower reversing mill drive.

Synchronous Motor Drives

It is hard to realize that 25 years ago the use of synchronous motors for driving rolling mills was actually unknown. Since 1925 they were successfully applied to a great variety of mills; continuous skelp, billet and rod mills, usually driven through a line shaft; tube piercing mills; roughing stands of some hot strip mills, and many others.

Prior to that time only slip ring induction motors would be considered for such constant speed drives. Now the use of

synchronous motors is rather the rule, while that of induction motors is the exception, which is followed only when flywheels actually are needed for the sake of peak load limitation.

The successful entry of synchronous motors into this field was due, primarily, to a better design of starting and running characteristics. A hot rolling mill is never started under load, that is, with the metal in the rolls. A starting or break-away torque of 50 per cent normal, or less, is usually sufficient, with few exceptions such as rod mills, where torque values up to 75 per cent might be needed. These torques, or even higher ones, are not difficult to provide. It is important, however, to maintain a high "torque efficiency", that is, a high ratio of starting torque to starting current, both being expressed in per cent of normal values. Otherwise the current inrush when starting a large motor, while maintaining sufficiently high torque, might become excessive for the power system.

The maximum running or "pull-out" torque of 200–300 per cent normal, depending on the type of mill, can be readily provided.

Some very notable applications of synchronous motors were made, with ratings as high as 9,000 horsepower at 107 rpm, 6,500 horsepower at 187 rpm, and many others. Figures 5 and 6 show a mill layout and a 4,500 horsepower, 400 rpm motor driving a rod mill; a typical example of a constant speed mill, rolling a large tonnage of products of rather limited variety.[4]

From the viewpoint of synchronous motors it is rather unfortunate that their usefulness for rolling mill drives became well established only at the end of era of constant speed drives in general. Even as late as 1931, of all the new drives purchased 32 per cent were of constant speed type, while in 1947 this percentage dropped below 14. This shows the ever growing trend toward adjustable speed equipments.

Adjustable Speed Drives

The advantages of operating rolling mills through a range of adjustable speeds were well recognized even before the period which we are considering. Electrical engineers had developed suitable drives for such mills as were in use at that time.

The technological developments in the art of rolling, the redesign of mills for greater operating flexibility and speed, and the greater reliance of mill designers on the electrical equipment have radically changed the type of electric drives.

A quarter of a century ago many mills, even though electrically driven, were still of the type designed originally for steam engine drive. For instance, a merchant mill shown in Figure 7 has ten stands through which the metal passes consequently, being reduced from billets having a cross section of, 4 by 4 inches, or thereabouts, to small rounds, flats or angles. All ten stands are geared to a common lineshaft which is driven by a single motor rated 4,500 horsepower, 300/500 rpm. When such large capacity is concentrated in one or two drives, and speed adjustment not exceeding 2-to-1 is sufficient, it is usually most economical to build them of the a-c adjustable-speed type, either of the Kraemer or Scherbius system.[5] However, the growing trend during the last 25 years has been to split the total drive capacity into smaller units, to drive almost every stand by a separate adjustable speed motor (Figure 9), eliminating line shafts and many gears. With individual speed adjustment of each stand available, greater flexibility in selecting rolling schedules is feasible. Thus, a greater variety of products can be successfully rolled on the same mill While each of the driving motors is smaller, the total installed capacity is naturally greater than with a group drive, since no advantage can be taken of the diversity load factor of the several stands.

Such arrangement invariably leads to the choice of d-c drives, supplied with adjustable voltage power from one or more motor-generators. As compared with the a-c adjustable speed systems (if the latter were considered for these new mills), the multidrive d-c arrangement is simpler and is more economical. The over-all number of machines involved is smaller, the cost of high voltage switchgear is reduced, and the possible speed range is increased.

This explains the twilight of the speed-controlled a-c drives so popular some 20–25 years ago, and the ascendency of the d-c drives which came into practically universal use during this period.

The majority of these drives are designed for 600 volts and this voltage can be almost considered as "standard" for steel mill applications. A few 750-volt installations have been made. For small drives, about 200 horsepower and smaller, 250 volts may still be considered.

Use of Rectifiers

Since the use of d-c drives for rolling mill drives is so widely spread, it is natural to consider the application of rectifiers to supply power for these drives. Several successful installations of this type have been made, but it is far from correct to claim that the rectifiers have replaced the synchronous motor-generators.

The rectifier has the advantage of higher efficiency over motor-generators,

Figure 10. Two 3,500-kw 600-volt rectifiers supplying power to a hot strip mill

Figure 11. Old-fashioned sheet mills requiring large amount of hand labor

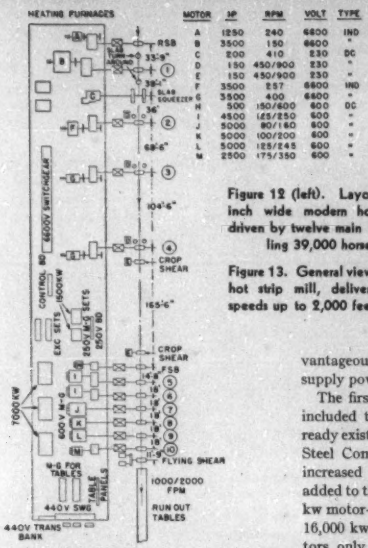

Figure 12 (left). Layout of an 80-inch wide modern hot strip mill, driven by twelve main motors totalling 39,000 horsepower

Figure 13. General view of a modern hot strip mill, delivering steel at speeds up to 2,000 feet per minute

particularly at 600 volts. Low running light losses are particularly advantageous. The installation costs are lower, since no heavy foundations are needed. Since rectifier losses are removed by cooling water, the problem of motor room ventilation is simplified. The absence of commutators, brushes, and bearings on the converting equipment lowers the maintenance costs.

On the other hand, the rectifier is handicapped by a somewhat lagging power factor about 95 per cent, instead of the leading one on the motor-generator. This may or may not be of importance, depending on the over-all power factor of a given steel plant. Voltage control is very easily obtained with the generators. With the rectifiers it is necessary to use phase retard which lowers the power factor as much as it lowers the voltage, or, transformer taps should be used, which method adds to the cost and is not as flexible as the generator field control.

Since the normal rectifier transmits current in one direction only, it is not suitable for supplying power to reversing mills, nor to any mill which should be quickly decelerated and stopped. Any additional equipment to permit regenerative braking adds so much to the cost of the rectifier as to make it noncompetitive with motor-generator.

In spite of these inherent limitations, the rectifiers were successfully and advantageously applies in several cases to supply power to mill drives.

The first installation designed in 1939 included two 3,500-kw units for the already existing hot strip mill of the Weirton Steel Company.[6] The mill drives were increased in size, and the rectifiers were added to the already installed three 3,000-kw motor-generators, providing a total of 16,000 kw on the 600-volt bus. Generators only are used for starting up the motors (which are never loaded when starting), and the rectifiers are then automatically connected to the bus. The voltage maintenance and the load division between the rectifiers and the generators are easily maintained by amplidyne regulators.

A merchant mill of the Kaiser Steel Company is driven by several d-c motors, rated from 300 to 1,500 horsepower. The power is supplied by a 2,500-kw, 600-volt rectifier. Phase control is used for starting the motors. Motor field control is used, as at Weirton, for adjusting the relative speeds of the several motors.

Two continuous tube mills are now under construction and each will be driven by a number of 600-volt d-c motors. The power will be provided by a 3,500-kw and a 1,500-kw rectifier, respectively.

Thus, the rectifier will, undoubtedly, find its legitimate, but limited, place in this field. It is doubtful whether it will replace largely the motor-generator for supplying power to the rolling mill drives.

Hot-Strip Mills

In no branch of the steel industry has the role of electric drive been so prominent as in the hot and cold strip mills. The rolling process made possible by these mills has revolutionized the industry to such an extent, and the process itself is, in turn, so dependent on electrical engineering that it deserves a discussion even in this brief review.

The metal fabricating industry is clamoring for more and more flat rolled steel, known in the trade as sheets, strip, and tinplate. Automobile bodies, freight cars, refrigerator cabinets, and other appliances, electric sheets, oil drums, and various containers, tin cans, and thousands of other applications, continuously growing in number, present their demands to the steel industry. In 1947 close to 29 per cent of all steel belonged to this class.

Twenty-five or more years ago a typical sheet mill, as shown in Figure 11, required a large amount of hard labor and the process was expensive. A sheet bar having dimensions of about, 8 by 3/8 by 24 inches, reheated in a furnace, would be passed several times through and over the rolls, requiring manual handling at either side by skillful operators. One or more reheatings would be needed before the sheet of proper width, length, and thickness would be obtained. Several sheet mills would be located side by side, as shown, driven at constant speed by an induction motor with a heavy flywheel.

A large amount of hand labor made the process expensive. Low tonnage output of then existing sheet mills could not keep up with the growing demand. The answer was provided by the development of strip mills.[7]

The art of rolling wide flat sheets, several hundred feet in length, has been developed within the last quarter century. The width of these sheets may vary from 30 to 96 inches. The finished thickness is usually between 1/16 inch and 1/8 inch. Such wide sheets of steel are delivered from a modern mill at a maximum speed of 2000–2200 feet per minute.

Figure 12 shows a typical layout of such mill. Starting from the top of the diagram, a heated slab, some 4-inches, 5-

Figure 14. Motor room of the hot strip mill

Three 7,000 kw motor - generators supplying power to d-c motors driving the finishing stands

inches, or 6-inches thick, passes through the roughing stands 1 to 4. These are so spaced that the same slab is never in more than one stand simultaneously. Thus, there is no need of providing a definite speed relationship between these roughing stands, and they are conventionally driven at a constant speed by either induction of synchronous motors. (In a few cases a reversing mill, similar to a plate mill, is used instead of the several roughing stands shown on Figure 12.) The slab emerges from the roughing stands elongated to 100–150 feet, reduced to a thickness of about 3/4 inch, more or less, and is then passed through a group of finishing stands, usually six in number. These stands are set on close centers, 18 to 20 feet, and quite obviously, the same sheet may be in all of these stands simultaneously. Thus, the rolling speed of each consecutive stand should be increased, as the thickness of the strip is reduced by the rolls. To provide for a wide variation of rolling schedules, calling for different thickness reductions or "drafts" on each stand, it is important to drive these stands individually by separate motors, with independent speed adjustment of each. Motors of 2,000 to 5,000 horsepower are usually employed to drive each stand.

In this case, again, the most economical and practical solution is the use of d-c motors, each with independent speed adjustment by field control (2-to-1 or more), supplied with power from several adjustable voltage generators.

The concentration of power at the finishing end of a large hot-strip mill is as high as anywhere in the industry, with the possible exception of some electrochemical processes. A mill illustrated in Figure 13, has a total of 27,000 horsepower of d-c motors supplied with power from 21,000 kw of 600-volt d-c generators. The problem of handling over 40,000-kw or close to 70,000 rated amperes of d-c power on a common bus is no mean task, considering the possibilities of fault current. Efficient as the electric machines are, it is not unusual to find that the total losses of motors, motor-generators, and auxiliary equipment, within a hot-strip mill motor room, are as high as 5,000 kw. To dissipate these losses and to keep the ambient temperature reasonably low the modern practice calls for a recirculating ventilating system. The hot air from the machines is discharged downward into the ducts in the basement, is passed through surface coolers (air-to-water heat exchangers), and is then pushed by fans into the motor room, through the machines, and so forth. Thus, the losses are "piped" away by water.[8]

The nature of the hot-strip load is cyclic. The speed of rolling is usually determined by metallurgical factors, and is usually higher than dictated by merely tonnage requirements of the mill. Thus, the slabs follow each other with certain time intervals between them. The mill load, starting with friction load (see Figure 14) gradually rises as the slab enters in succession the finishing stands. The peak load of, say, 30,000 kw or more will be reached in about ten seconds when all stands are full. After a brief interval of this maximum load, the latter gradually drops off again down to friction load.

The magnitude of this load and its cyclic character is apt to disturb the voltage regulation on the power system, even when the steel plant is tied with a large utility. Assume, for instance, that the d-c generators are driven by synchronous motors which operate with constant excitation. With the mill load changing from friction to over 30,000 kw, the reactive kilovars given out by the synchronous motors are going down by about 15,000 kva, which is opposite of what is needed for good voltage maintenance. Hence, the industry has adopted in the last ten years a practice of automatically controlling the excitation of these synchronous machines as function of their load. In addition to improving the system voltage regulation, this method by reducing the running light losses may easily save some 300,000–500,000 kilowatt-hours per year for a large strip mill.[9]

Many other refinements have been contributed by the electrical industry to the art of rolling strip.

The thickness of the hot strip should be checked while it emerges from the last stand at a speed of some 2,000 feet per second. Any measurement involving contact with the fast-moving hot metal is out of the question. An X-ray gauge has been developed for this purpose.

A suitably protected 100-kv X-ray tube is located under the moving strip. Part of the rays pass through the strip, another part through a selected "standard" of thickness. The comparison of the two values indicates to the operator any deviations from the standard.[10]

In many cases the moving strip should be sheared to uniform length as it comes off the mill. The length of cut may vary as much as two-to-one. A rotary type "flying shear," mounted at the finishing stand, is driven by separate motors. By changing the ratio of shear speed to that of the strip, the length of cut is adjusted. Once adjusted, it should be maintained with an accuracy of about 1/4 of one per cent. An electronic-amplidyne speed regulator is successfully doing this job.

Other problems of handling hot strip after mill delivers it, such as the operation of runout tables, coilers, and so forth, are also solved by electric means, but space does not permit going into this subject.[17]

It is obvious that from a practical viewpoint a modern hot strip mill could not be designed, built or operated successfully without electric drives. As a matter of fact, it is designed usually around the electric equipment. Thus, this type of mill which so materially reduced the cost of flat rolled steel, and made the quantity production of sheets for automobile and many other uses economically possible, owes its very existence to the electric equipment.

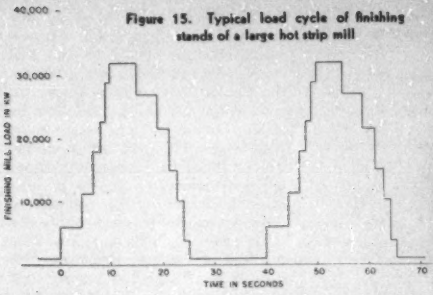

Figure 15. Typical load cycle of finishing stands of a large hot strip mill

Figure 16. Modern 5-stand tandem cold strip mill

Cold-Strip Mills

The Process. A still greater contribution to the steel industry was made by the electrical engineers in the field of cold rolling of steel after it has been delivered from the hot strip mill.

This strip steel, delivered in a coil form, is passed, first of all, through a so-called pickling line (acid bath) to remove the scale and other surface blemishes. Recoiled again, it is delivered to a tandem cold reduction mill.

A mill for rolling wide sheets may include three stands, reducing the strip in three passes from a thickness of 0.109 to 0.037 inch. A mill for producing tin plate may include five tandem stands, and will probably reduce a strip about 30-inches wide from a thickness of 0.072 to 0.010 inch, or thereabouts. The strip is wound up in coil form on a tension reel as it is delivered from the last mill stand.

When a limited tonnage, or a great diversity of products is needed, the cold reduction is economically done on a reversing cold mill. It consists of a single mill stand, with a winding reel at either side. By passing the strip back and forth through the mill, taking reduction for each pass, the strip is given the needed thickness. The reels, each driven by separate motors, alternate as a winding or an unwinding unit.

The strip is then passed through a cleaning line to remove oil picked up during the cold rolling. Wound in the coil form again, the strip is annealed in furnaces to remove the hardness created by cold rolling.

The next operation is to give the strip the proper finish and needed physical property; for this purpose the metal is passed through a "temper pass" or "skin pass" mill. The latter consists of one or two stands, with paying-out and winding tension reels. Very little reduction in thickness is given to metal on this mill, usually less than five per cent.

After this operation the strip is given the final processing, such as galvanizing, tin plating, and shearing.

The operating speed of cold strip mills has been raised in a spectacular manner within the past 20 years. In 1928 a tandem mill was built whereby the conventional speed of about 200 feet per minute was nearly doubled. In rapid succession the mill speeds were raised to 600, 800, and 1,000 feet per minute. In 1933 the first 5-stand tin plate mill was built with a top speed of 1,200 feet per minute. By 1938 several mills were operating close to 2,000 feet per minute. In 1941 the top speed of 3,800 feet per minute was reached. The war years have merely delayed the race, and it was resumed after the end of hostilities. Several tin plate mills were built, and some of these are in service, with a rated top speed close to 5,000 feet per minute. Finally, a 5-stand mill (discussed later) was designed and has been operating at a speed of 6,250 feet per minute, or 71 miles per hour.

This striving for higher speeds is, of course, based on economic factors. With substantially the same operating crew, a 5,000 feet per minute mill will probably produce double tonnage as compared with a 2,000 feet per minute mill. Even though the mechanical and electrical equipment is more expensive in the former case, the over-all operating cost per ton should be lower, everything else remaining constant. This is the industry's answer to the higher wages of steel mill workers.

Electric Equipment. Each stand of a cold strip mill and the tension reel are driven by separate d-c motors. The power is supplied from one or more generators. Generator voltage is used to speed up or slow down the mill as a unit. Motor field control is employed to readjust the relative speeds of the several stands, to take care of various rolling schedules.

For each new coil the mill is slowed down to a threading speed, in order to bring the strip through the several stands, to make a few wraps on the winding reel, and to make certain that the metal is under tension between the stands and at the reel. Then the entire mill is accelerated to its selected maximum speed, still maintaining substantially the same strip tension. If the latter condition is not met, the expected reductions of strip thickness will not be realized. The strip will be "off gauge." Furthermore, even with the same tension and the same setting of rolls ("drafts"), the gauge is apt to be different at high speed than it is at low speed. Hence, the logical desire of the operators to accelerate or to retard the mill as fast as possible, in order to reduce to the minimum the amount of "off gauge" material rolled.

These operating conditions impose rather severe requirements on the electric equipment. To accelerate several motors simultaneously while they are held together only by a relatively thin strip, to maintain their relative speeds during this period, to maintain also a fairly constant tension on the winding reel are a few of the problems faced by electrical engineers.

The first task is to suitably design the mill motors; that is to bring their inertias (WK^2) down and, at any rate, to match as nearly as possible the WK^2 of the several motors. This often means splitting the motor capacity at each stand into two or even four armatures.

For any given rolling schedule the motors may not be equally loaded so that the IR drops of their armature circuits may not equal. Thus, their relative speeds, if properly matched at low voltage, may not match at maximum speed and voltage, or vice versa. Suitable compensation for

IR drop must be provided in the control. An up-to-date arrangement is to provide a separate generator for each of the mill motors and for the reel, thus avoiding a common bus. In spite of this segregation the voltages of the several generators are quickly built up or are quickly reduced, maintaining the selected relationship.[11]

Figure 17 shows diagrammatically the arrangement of the high-speed tin-plate mill which has been operated at speeds in excess of 6,000 feet per minute. The ratings of the several motors are indicated on the sketch. The total installed capacity of the drives is 16,850 horsepower, backed-up by 14,200 kw of generators, mounted on two sets. Each mill and reel drive has its own generator.

The 1,750 and 3,500 horsepower drives are direct connected to stands 1 and 2, respectively, through a set of pinions. In other words, the motors run at the same speed as the rolls. The 3,500 horsepower motor is built as a double armature machine. The drives for the stands 3, 4, and 5 are built as twin drives, that is, the top and bottom working rolls are driven by separate motors, tied together by electrical means only. Thus, the conventional mill pinions are eliminated. To reduce the stored energy of rotating masses it was found advantageous to run the motors at a lower speed than the rolls. Step-up gears are employed between each motor and the respective roll. The presence of such gears also permitted the location of the motors on suitable centers. For the stand 5, each of the twin motors is built with two armatures on the same shaft, in order to keep the total moment of inertia still lower.

To operate these several motors and mill stands in unison, without tearing the thin strip apart and without affecting its gauge, obviously requires a carefully laid out control system. The heart of the latter is, again, the high-speed rotating amplifier and regulator, known as the amplidyne. A number of these amplidynes are used for building up or maintaining the voltages of the several generators in the selected relationship, for dividing properly the loads between the twin motors, for maintaining constant strip tension on the winding reel and for several other purposes. It can be said that in case of a modern cold strip mill the electric equipment acts as the "brains and brawn" of the process. Without the electric equipment the process itself would not be feasible.

Since the strip is under tension during rolling, it is important to give the operators some ready means of measuring and indicating this value. Here, again, electric means are provided with good effect.

A spring-mounted roller is located between the adjacent mill stands, or between the last stand and the winding reel. The roller deflects the strip slightly upward (2–3 inches) from the pass line. The higher is the strip tension, the stronger is the downward push on the roller against the supporting springs. The latter are rather stiff, permitting a maximum vertical travel of about 1/8 inch. This travel is proportional to the strip tension. By measuring this travel by well-known electromagnetic means, such as used in electric gauges, the strip tension can be readily indicated by electric instruments. In this manner the mill operators are always fully aware of the pull exerted on the strip.[13]

Auxiliary Drives

The rolling mill drives are not the only motor applications in the steel industry. As a matter of fact, their over-all installed capacity, great as it is, is less than half of the grand total of motors in the steel industry.

Motors for pumps, compressors, and fans, of which a steel plant needs quite a few, are not much different from similar motors in other industries. The same applies to motors in coal preparation plants, coke oven by-product plants, and like. A-c motors are usually employed.

Blast furnace blowers are, as a rule, steam turbine driven. They are usually located next to the source of waste gas and to the power plant. Automatic control of blast is required, which usually means speed adjustment. Thus, the use of steam drive is logical, instead of conversion of steam to electric power, and then again to mechanical power at the blower shaft.

Bessemer blowers may be in a different category since they are often located remote from the powerhouse. A number of these blowers are electrically driven. Slip ring motors with secondary control were originally employed to provide speed adjustment. Some of the recent blowers were designed with mechanical means of controlling the air volume. Constant speed drives become feasible, making it a very good application for synchronous motors rated about 4,000–6,000 horsepower.

The most specific motor applications in steel mills are the so-called auxiliary drives usually employed for handling metal during and between the various processes. Mill screwdowns, mill tables, manipulators, and transfer cars, as well as cranes, are examples of this class.

Since the early days of electrification the steel industry has chosen the heavy duty d-c motor for this application. The

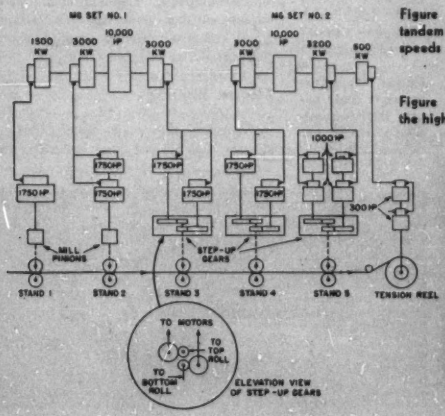

Figure 17 (left). Layout of a tandem mill rolling cold strip at speeds up to 6,000 feet per minute

Figure 18. Motor room of the high-speed cold mill shown on Figure 17

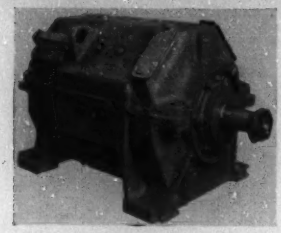

Figure 19. Heavy duty d-c motor used for mill auxiliary drives and cranes

inherently better motor characteristics and superior control as compared with a-c drives were the basis of this choice. These factors remain as strong today as they were 50 years ago.

Since 1926 the Association of Iron and Steel Engineers standardized on ten motor ratings arranged in logical steps from 5 to 150 horsepower. For one given rating there is only one rated speed. Series, compound, and shunt motors were available, all ratings based on 230/250-volt power supply. Mounting dimensions were standardized so that complete motors of the same rating, regardless of make, could be interchangeable.

In 1947 the Association adopted a new standard, which maintains the same mounting dimensions but provides for each motor frame size the next larger rating, as compared with the 1926 standard. This has been obtained by better utilization of available space, without permitting any higher operating temperature, nor sacrificing the motor ability to handle high peak loads.

While the popularity of d-c motors persists, and even grows, the method of supplying power to them is undergoing a marked change.

The previous universal practice was to provide a constant potential bus supplied with power from 250-volt motor-generators. The 1,000 and 1,500 kw units were most common. These sets were located at strategic points in the steel plant. The d-c motors were conventionally equipped with magnetic control. The growing tendency is to provide many auxiliary drives with separate generators and to employ the generator voltage or Ward Leonard control. For instance, every new blooming or slabbing built in the last 12 years has been equipped with Ward Leonard control for its principal auxiliaries: reversing front and rear tables, right and left side guards, screwdowns, and bloom shear. Each of these drives usually employs one or two motors of 150–250 horsepower capacity and a separate generator of suitable capacity. Such applications as flying shears, runout tables, strip coilers to name a few, also fall in this class. The several generators are assembled customarily together in groups of four or even six and are driven by a common synchronous motor.

The advantages of this system are many. Heavy reversing and accelerating contactors in the armature circuit are replaced by small devices handling merely the generator field. Smoother, yet faster, acceleration of the drives is possible. The 230-volt mill motors can be readily operated at double voltage, thus permitting higher output per frame and higher maximum operating speed. Thus, lower electrical and mechanical maintenance and superior performance are realized.[13]

The adjustable voltage systems have found a very fertile field in the numerous processing lines, such as pickling, cleaning, galvanizing, and shearing, which are encountered in any cold strip mill plant.

This tendency toward the greater use of Ward Leonard control reduces the load on the 250-volt d-c bus and transfers it to the a-c power system. At the same time the advantages of d-c motor drives are maintained and are even enhanced.

One should not conclude that the constant potential 250-volt d-c power supply in steel mills will disappear. There are too many drives, such as numerous cranes and auxiliaries, where Ward Leonard control is not economically feasible, nor is even needed. Therefore, the need of a-c to d-c converting units operating at constant d-c voltage will be always present in a steel mill.

The development of single anode ignitron rectifiers which gives a low arc drop and, therefore, good efficiency even at 250 volts, has challenged the monopoly previously held by the motor-generators. The steel industry has, in effect, adopted the arc rectifier in ratings of 1,000 and 1,500 kw as a preferred equipment for this purpose.

Electric Heating

There are two main applications of electric heat in the steel industry, arc

Figure 20 (left). General view of typical strip processing line

Individual d-c motor drives with Ward Leonard control are used. This line is used for trimming and shearing

Figure 21. Continuous electric furnace for heat treatment of silicon and stainless steel strip

furnaces for melting metal, and resistor and induction furnaces for heat treatment. Since the cost of electric heat is higher, on the mere thermal basis, than that of other fuels, its use is competitively justified when other savings like reduction of rejects or higher quality of product can be realized. There are many such applications.

Arc Furnaces

Electric steel, melted and refined in these furnaces, is considered to be of higher and more uniform quality than the commercial grades. An electric furnace, being on the average smaller than an open hearth furnace, lends itself better to production of smaller lots or "heats" of special steels. Hence, it is employed mainly for the making of the more expensive steel alloys.

The demand for these alloys and the tonnage of electric steel rise together. The annual capacity of electric furnaces in 1935 was 1,050,000 tons. Just before the World War II it rose to 1,500,000 tons. In 1945 it was close to 5,500,000 tons. This spectacular rise was primarily due to the intensive use of high tensile strength and other special alloy steels in war production. Some of this arc furnace capacity is not fully utilized in postwar steel production.

Electric arc furnaces had been built in capacities as large as 100 tons, although a 35–50 ton unit is more representative of the large furnace class. An average American ingot-producing furnace gives about 35,000 tons annually. A good average value of power consumption is about 600 kilowatt hours per ton.

The primary side of the furnace transformer is usually connected to the high-voltage system of the steel plant, up to 34.5 kv. No-load taps and wye-delta connections on the transfer primary winding are customary.

The most interesting innovation in electric equipment for furnaces is the introduction of rotating regulators for the electrode drives. A d-c motor is used for each electrode. It is supplied with power from a separate amplidyne generator (or equivalent). The signals from the electrode current and voltage control the amplidyne fields and cause the motor to function suitably. This type of control has proved to be accurate, fast, and yet stable and smooth.[14]

Resistor and Induction Furnaces

The percentage of steel-mill heat-treating furnaces employing electric heat is not very high. However, many notable installations were made where precision quality control or other inherent advantages of electric furnaces were of most importance. A few examples will illustrate this point.

Figure 21 shows a 450-kw furnace for heat treatment of silicon and stainless strip steel at temperatures as high as 2,000 degrees Fahrenheit. In this case the strip speed is rather low, less than 10 feet per minute. Two continuous strip processing lines are being built, each to include a 2,000 kw resistor furnace as a component part, and speeds up to 200 feet per minute are provided. On another and slower processing line for heavier strip, the latter passes through an induction coil energized by two 700-kw, 9600-cycle generators, and is heated to a temperature of about 1,300–1,400 degrees Fahrenheit.

During the wartime shortage of tin the steel industry had to change the method of applying tin coating to tinplate from the hot-dip to electroplating process. The coating is applied to a strip moving at a speed up to 1,000–1,200 feet per minute. Plating currents up to 60,000 amperes at nine–ten volts are provided either by generators or dry-plate rectifiers.[15]

The electroplating tinning saves better than 50 per cent of tin as compared with the hot-dip method. However, the finish is not as bright as desired. A so-called "flowbrightening" process is, therefore, added to the processing line immediately following the tin-plating. The idea is to heat the coat of tin moderately, causing the tin to "flow" and to give thereby a smoother finish. Electric heat has been successfully applied for this purpose in two different manners.

Under the first method 60-cycle alternating current is carried by the portion of the strip to be heated. Practically 100 per cent conversion to heat is realized. Current collection problem has been satisfactorily solved.

The second method consists of using high frequency for heating the moving strip. Radio frequency of over 200 megacycles has been used on several installations. Since the strip moves at high speed, it becomes necessary to concentrate huge blocks of power in the high frequency transmitters, up to 1,000–1,200 kw on a single processing line.[16] It is claimed rightly that more radio frequency capacity has been installed for this purpose in the steel industry than is used in all broadcasting stations put together. These equipments are performing very satisfactorily even though they are rather expensive. Tests were also made, with very good results, to apply for this purpose induction heat of lower frequency, like 9,600 cycles. This will very materially reduce the first cost of the installation.

Conclusion

The partnership of electricity and steel has been quite fruitful. Electric power has contributed a large share toward economical mass production of steel, on which our entire industrial life is based. If the last quarter of a century is any criterion, we should expect that in the next 25 years the art of steel making will be allied still closer to electrical engineering.

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Trends in Electron Tube Design

W. C. WHITE
FELLOW AIEE

THOMAS A. EDISON, John A. Fleming, Lee DeForest, and the other pioneers in the development of the electron tube found it necessary, of course, to do much design work, but quantity production of many thousands of tubes to meet a specification for use in standardized pieces of equipment appears to have been first encountered by the French in the early part of World War I. When the military communication needs arose at that time and the decision was made to adopt small electron tubes, it was natural for them to turn to the manufacturers of incandescent lamps for production requirements (Figure 1). These lamp makers under the stress of war utilized wherever possible parts and techniques of lamp manufacture. These techniques persisted for many years in tube design and a few features of lamp design still are retained. Many tube manufacturers today are also makers of lamps.

Tube designers and their associates have always been a restless, resourceful group constantly adding new types and forever changing materials, designs, and processes in an effort to expand still further the field of usefulness of tubes; to lower their cost and to improve their quality as well as performance. This fact must be kept in mind because by the time this paper is presented, some statements herein already may be out of date.

During the more than 30 years that tubes have been designed on an engineering basis and manufactured in quantity, there have been innumerable changes in tube design. It is not within the scope of this paper to trace this history but rather to review briefly a number of the present broad design features of electron tubes and, in particular, to describe trends that designers are thinking and worrying about and thus give clues as to what changes may become increasingly apparent in the next few years.

There are a number of ways in which such a study might be arranged. It seemed best to divide up the broad subject of tube design into a number of subdivisions based on certain fields of tube applications. In doing this, however, it must be kept in mind that the term "electron tube" is now a broad descriptive term. Their size and weight range from a unit that could easily go astray in a vest pocket to one requiring the aid of a crane in moving it about the factory (Figure 2). Also, there is little in common, except tightness, between a sealed steel-envelope ignitron and a glass radio-receiving tube. Therefore, from the viewpoint of an engineer specializing in one particular class of electron tube, this paper well may be meager in its coverage, but it is hoped that it will give engineers who look at tubes fundamentally as useful tools in electrical engineering a picture of where and how this science is changing and progressing.

General

In any study of tube design, the enormous continuing expansion of new and different types of tubes and their application must be kept in mind. It cannot be expected that a tube suitable, for instance, for the control of an electric blanket can be used in an intricate computing instrument even if the current and voltage relations are similar. There also will be a growing number of unusual and highly specialized devices utilizing the flow of electrons or positive ions. Examples are the Geiger counter, television camera tubes, electron microscopes, betatrons, and electron diffraction instruments. This paper will not even attempt to list all of these special devices but will discuss only trends in the broad classes of tubes which find wide usage. In general, only trends in the Americas will be included. Each of the various fields of application of electron tubes places a different emphasis on the many design factors involved. For some special applications, even a small tube may cost $100 or more and yet its use be highly justified. On the other hand, a tube selling at such a figure, no matter what its capabilities, could not possibly be considered for a home broadcast receiver.

As in any new engineering development, there are differences of engineering opinion on certain aspects of a subject. Time, as usual, will resolve these technical controversies and the various factors will be fitted into their proper place.

Ever since the science of nucleonics has become a common subject of discussion, suggestions have been made that materials giving off alpha, beta, or gamma particles now available after irradiation in an atomic energy pile should find usefulness inside electron tubes. To date no such application has been disclosed and it would appear that more knowledge and research are necessary before this new science will be directly helpful to the tube engineer.

Broad Trends

In the early construction of electron tubes, all of the electrodes were made of pure metals. The science now has progressed to the point where this is no longer true and in nearly all cases the electrodes have a special surface coating over a base metal in order to provide a maximum usefulness.

Electronics continues to be a science of extremes. Tubes commonly are employed throughout the frequency range between 25 cycles and 30 billion cycles per second. As has been noted, there are enormous differences in size. Tubes are available that will detect a current of 10^{-16} amperes or deliver a peak current of

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W. C. White is with the research laboratory of the General Electric Company, Schenectady, N. Y.

Figure 1. The French World War I all-purpose radio tube; manufactured on a production basis in 1916

10^4 amperes. In a similar way, tubes will respond to potentials of a few microvolts and yet the modern betatron deals with a hundred million volts.

Another interesting feature is that the engineer in no other highly specialized field utilizes regularly so many of the chemical elements including several of the rare ones. More than 30 of the elements commonly are employed by the tube engineer and they span the alphabet from argon to xenon and zirconium.

The frequencies at which tubes are operated are not a factor in their design in the low end of the spectrum of electromagnetic radiations. As the frequency increases, its importance in connection with the design of the tube increases. In the range around 100 megacycles, it has become such an important factor that part of the tube must be considered a part of the circuit. This becomes increasingly true as the frequency is increased still further into the microwave region where the tube structure is often also the circuit.

Another trend apparent during the past few years is that most of the new outstanding advances in electronics have taken advantage of our increasing knowledge of electron beams or what is termed the science of electron optics. Examples of this are the betatron,[1] phasitron,[2] radial beam tubes,[3] cyclophon,[4] traveling-wave tubes,[5] television camera tubes,[6] (Figure 3), memory tubes,[7] and others.[8–10] The author believes that this trend will be accelerated and it may well be that almost all of the really new and worthwhile advances from now on will be the result of our increased understanding and application of electron optics. Young engineers studying for or just entering the field

Figure 2. A high-voltage high-power sealed ignitron used for power conversion

Rated for 150 amperes at 20-kv inverse voltage

of electronics as well as laboratory groups, well might keep this in mind. The number of engineers and physicists with a thorough knowledge of this branch of science plus the other factors needed for successful research and development are entirely inadequate to the possibilities that exist.

Radio-Receiving Tubes

Miniatures and Subminiatures

Engineers in this field in visiting one another do not delay very long before they are discussing miniatures and subminiatures. From the days of the "peanut" tube of World War I the desire for smaller sizes of tubes has existed but it has taken time to solve the problems of maintaining quality and performance without additional cost. How far the trend towards ever smaller sizes is going is anyone's guess. Press releases from a United States government laboratory of late have described tubes of the "grain of rice" size.[11]

Both the tube and receiver manufacturers have good reasons for wanting smaller tubes. The tube manufacturers see this trend as a basic way toward lower costs because less material is involved and it should be possible to keep the other cost items at about the same or even lower level. Additional advantages are possibilities of speeding up certain manufacturing processes and the minimizing of space requirements for packaged tubes all the way from factory warehousing to the dealers' shelves.

The manufacturers of receivers are working constantly toward a lower cost for their product and smaller tubes mean smaller chassis and cabinet dimensions with accompanying savings in material. In addition there are sales appeal features and fewer problems from microphonics, less breakage in handling and shipment, and better high-frequency performance.

Miniatures[12] have been available for nine years or more but originally were designed and utilized almost entirely in battery-operated portable radio receivers and other devices having the same general requirements.

Most miniature tubes are built with tubular bulbs, the trade designation for which bulb is *T* followed by a numeral which is the diameter of the bulb in eighths of an inch. This is not to be confused with the assigned group of designations such as *6T8* and *12AT7* where the *T8* and *T7* have no significance as regards the bulb diameter.

Most receiving tube engineers classify tubes with *T5* to *T6*$^1/_2$ bulbs as miniatures with a further general understanding that such tubes have basic electrode structures not greatly different in size from similar standard larger tubes (Figure 4). The reduction in dimensions of miniatures is accomplished largely by the elimination of wasted space and unnecessary parts from similar large tube designs. This is indicated in Figure 5 where a modern miniature of a World War II development is shown to be small enough to slip inside the hollow stem of a tube built during World War I. The smaller tube has superior characteristics, however.

Subminiatures have diameters below a *T5* bulb and some go down to as small as *T1*$^1/_2$ bulb (Figures 6 and 7). At present subminiatures are used almost exclusively in special applications such as hearing aids, very small portable receivers and certain military applications where space and weight requirements are extreme.

World War II military requirements were powerful influences in the development of subminiatures, the best example being the tubes used in vast quantities in connection with the proximity fuse.[13]

Another factor accelerating the trend to smaller dimensions is the use of tubes in relatively large numbers for accomplishing a desired result and, therefore, the space occupied by the tubes becomes important. Examples of this are modern electronic computers which may employ several thousand tubes as well as television receivers having several dozen.

In spite of the size of miniatures, the practice of including several electrode structures in one bulb continues. As an example, there is now available inside one miniature sized bulb the combination of three diodes and a triode.

Some subminiature tubes have flattened bulbs. In general this is believed to be desirable only for some special application such as hearing aids. Most makers of tubes look askance at bulb structures that are not symmetrical around a vertical axis. This is due to the fact that so much of their automatic manufacturing equipment is predicated on this fundamental geometrical feature.

Miniature tubes are not applicable for all positions in modern radio receivers because the necessary heat dissipation for some design requirements is excessive for these small bulb sizes.

Seven- and 9-pin moulded glass headers are standard for miniatures in the industry. For many sub-miniatures, wire leads are substituted for pins.

MISCELLANEOUS

For applications in battery-operated equipment, filament power is down to 50 milliamperes or less from a $1^1/_2$ volt dry cell. For some special uses, tubes having even lower filament currents are available.

In radio receivers, pentodes still are generally used for radio-frequency and superheterodyne intermediate frequency amplifiers and pentagrid tubes are employed almost universally, as converters.

There are now about 500 types of receiving tubes listed, and of these more than 50 are miniatures. There seems to be no question but that the number of types is going to increase at an accelerated pace due to the requirements and developments in television and frequency-modulation broadcasting, as well as other special uses.

The demand for higher values of transconductance which has confronted the tube designer from the beginning continues, and associated with it is the accompanying need for lower output impedance values in order to give more uniform response over a broadband of frequency. As a result, the tube designer has been forced to much closer interelectrode spacings, particularly between cathode and grid where three mils or even less is now common. As has been indicated, standard sized tubes still are used for some functions in receivers where the heat dissipation is too high for the smaller sized bulbs of the miniatures. Examples of this are rectifier tubes for large receivers and tubes for deflection of the beam in picture tubes as well as for certain audio-amplifier circuits.

In many standard sized receiving types, the steel envelope design continues in common use. New designs of receiving tubes are relatively infrequent in the large standard size, so probably there will be few new designs of the larger metal tubes in the near future.

Some Manufacturing Features

The use of coated filaments applied in the form of the carbonates of the alkaline earth metals is almost universal.

Grid wires commonly employ molybdenum or its alloys. Such wires are used in diameters as low as one to two mils. The minimizing of primary emission from the grids is usually accomplished by some special surface treatment, each manufacturer having his own favorite technique.

The anodes are mostly of iron or steel with some surface treatment. A new material, proposed for this use, is aluminum-coated steel. This is built into the tube as a bright surfaced metal but upon being raised to a bright red heat during evacuation of the tube, it suddenly changes to a dark gray color. This is a very useful characteristic as it permits the freeing of the anode metal from gas with reasonable amounts of heating power due to the poor radiation of a shiny surface. In the finished tube, however, it provides a dark and thus high heat radiating surface permitting lower anode temperatures. The basic idea seems to have come from the Germans and is one of the two worthwhile new ideas that were initiated in enemy countries during World War II. The other is ceramic material suitable for tube seals and envelopes. Although this latter is an important new development on which tube engineers are very active in the United States, as mentioned elsewhere, it is not being applied as yet to receiving tubes.

Barium-content getters are almost universally employed. Zirconium, although in fairly common use for transmitting tubes, is not being applied commercially to receiving tube production.

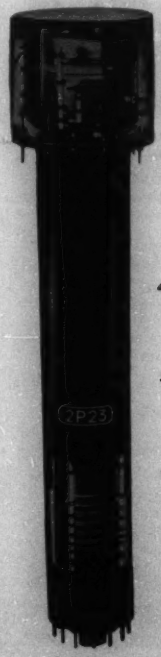


Figure 3 (left). Image multiplier Orthicon as used in television cameras for outside pick-up

Figure 4. The miniature tube (at right) has an electrode structure nearly as large as a standard sized tube

The smaller size is accomplished mostly by eliminating unnecessary parts and space

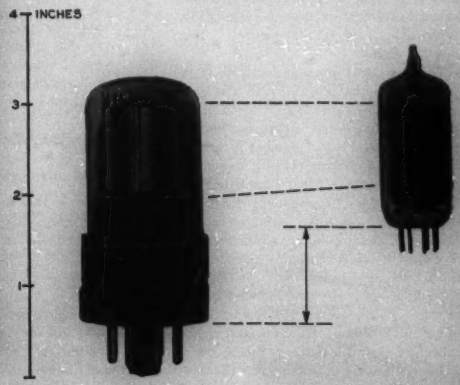


Figure 5. A World War II tube slips easily inside the stem of one built during World War I, yet the smaller one shows better performance

Most receiving tubes are designed for operation of the cathodes at 6.3 or 12.6 volts although there are many other voltages utilized for special cases. Tubes designed for 110-volt heater operation would be welcomed by many receiver designers but several difficult problems are involved and this feature is not particularly favored by tube engineers and, except in very special cases, probably will not be used to any great extent.

Most radio receiving tubes are operated at plate voltages under 150 and a common maximum rating is about 300.

Ever-Present Problems

The tube designer constantly is confronted with the problem of cutting tenths or even one-hundredths of a cent out of the cost of a tube. This is accomplished either by redesign or lower manufacturing losses. The mechanization of tube manufacture is an important phase of cost reduction. It is going on actively step-by-step in all the operations involved. Complete machine assembly of tubes on a commercial basis still appears to be a future development.

Two other problems confronting tube engineers is the minimizing of grid emission and the decrease and control of electrical leakage between the heater and the cathode in tubes utilizing these features. The latter problem is a most difficult one because of the problem of designing a hollow red-hot metallic conductor about the diameter of the lead in a pencil into which is put another red-hot conductor with only two mils available for space in which to place insulating material, the whole having to operate in the reducing action of a high vacuum. This combination must be operated not only at 110 volts between the two elements without breakdowu, but the leakage resistance between the two must be above about 100 megohms per inch of conductor. Admittedly, this is quite a problem, particularly in devices which must be manufactured for a fraction of a dollar each in million lots. Tube designers are meeting this very difficult requirement by the use of alumina. Many close manufacturing controls are involved, however.

As discussed elsewhere, there is a constant urge to minimize early life failures and this can be done only by careful attention to scores of design, process, and material details. A companion problem is convincing receiver designers that tubes should be operated conservatively to minimize early failure and in some cases to obtain long average life.

Another favorite topic for discussion is when or when not to create a new type of tube to meet the real or fancied demand of someone with a new circuit or application. Most tube engineers would consider this a short and sketchy recitation of their many problems, but it at least gives engineers in other fields an idea of what some of the problems are.

New Problems

The tubes available for radio receiver designs are suitable in general for frequencies up to 216 megacycles, which is the top limit for present commercial television. However, tubes for the citizens' radio band (460-470 megacycles) and the wide frequency zone extending up to nearly 1,000 megacycles that has been set aside for color and other improved television systems presents a whole new series of problems. For these higher frequencies, some of the special tubes, such as klystrons, disk-seal tubes, and dyotrons, to be described later, may well enter the picture.

Future Problems

In discussing the future with men in the industry, a number of subjects are usually introduced. One is a need for a special tube for frequency-modulator receivers to combine the functions of discriminator and limiter and possibly include other useful characteristics. As

Figure 6. Sizes of subminiature tubes compared to a standard length cigarette and kitchen match

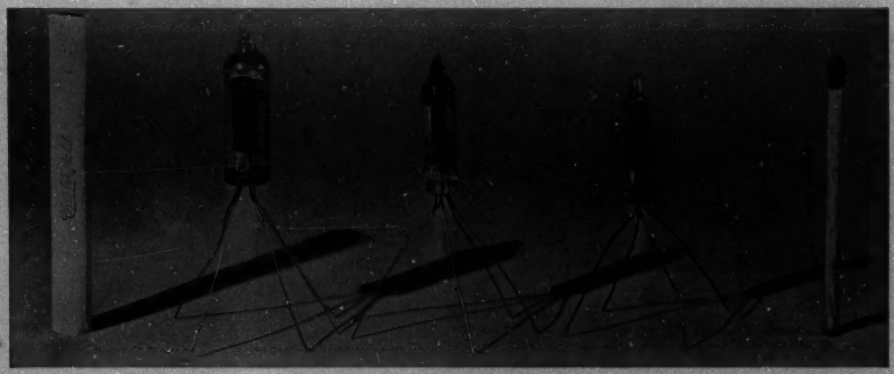


Figure 7. Comparison between T-1 and ST-12 types for size and simplicity of design

one engineer expressed it: "We know that someone is going to pull such a tube out of the hat, but we don't know from whose hat it is coming."

Although ceramics are not employed today in standard receiving tubes, this technique is being thought about as one possible way of building miniatures that will dissipate more power. There are reports that such tubes have been built in Germany and are operated at a temperature of several hundred degrees.

There is also some speculation on the substitution of tubes soldered into their circuits rather than utilizing bases and sockets. There are a number of factors favorable to such a future development. Some of these are the new printed circuits and the wire leads on subminiature tubes. There is also the fact that there is less tinkering nowadays with radio receivers in the home either by amateurs or professionals. The diagnosis and repair of faults in a modern radio receiver can best, and sometimes only, be accomplished in a well-equipped radio service shop. The advent of television receivers emphasizes this trend. It is also no easy matter for the designer of a television receiver to arrange for chassis units in which every one of the two dozen or more tubes easily can be removed and replaced, even with bases and sockets.

Small Receiving Type Tubes for Industrial Control and Instrumentation

For many years this field of application was supplied by using standard radio receiving tubes. In order to obtain from them the greater uniformity and longer tube life needed, it was customary to operate the tubes very conservatively as to plate voltage, plate current, and often cathode temperature. It also was believed that the low cost of receiving tubes and their almost universal availability were strong reasons for following this policy. Of late, however, opinions on this subject have been changing. This is attributable to a number of factors.

1. Even under favorable conditions, the life is often too short for use continuously 24 hours per day. Industrial users of tubes now talk about the life in years rather than in a few thousand hours and the elimination of early failures is of increasing importance.

2. Mechanically, some users (often mistakenly) believe that radio receiving tubes are not strong enough for their applications. They need not have the extreme sturdiness of the tubes used in proximity fuses[13] which were actually shot out of a gun, but they may incorporate to advantage some special mechanical design features for some of the new services.

3. Users of tubes for instrumentation in particular have been critical of the wide variation of electron current and other characteristics between individual tubes.

4. Many tubes have not been considered satisfactory as regards constancy of anode current during life.

5. The great increase in the extent and variation in industrial use has created new requirements.

6. From time to time a tube manufacturer has changed some electrical or mechanical design feature which has been unimportant from the viewpoint of radio receiver use but has introduced serious difficulties in certain industrial applications. Therefore, revised and more definite specifications may be looked for in the future.

A good example of this last factor was a time delay relay, the coil of which was operated from the plate current of a receiving type tube. The current and voltage requirements were very low and, as very long life was desired, the tubes were operated at subnormal cathode temperature. Satisfactory results were obtained with renewal tubes for a number of years but during the war apparently new cathode materials were substituted or a process changed so that, while the electron emis-

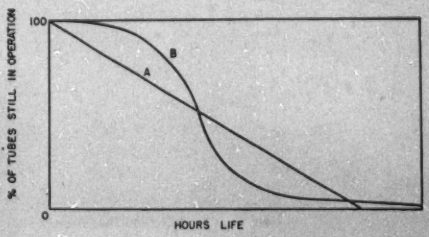


Figure 8. The mortality curve of vacuum tubes assumes a shape shown in B as design and quality are improved

sion was normal at rated heater voltage, it was entirely inadequate at the lowered heater temperature used in this industrial application. Therefore, renewal tubes of the later date of manufacture were inoperable in the equipment in question.

One manufacturer has announced a new line of small industrial tubes to replace similar radio receiving tubes. Initially there are three types in this line (not miniatures). Two of these are of glass and one has a metal envelope. These three new tubes are in general mechanically and electrically interchangeable with corresponding radio receiving tubes. They, of course, have different designations and incorporate improvements over their radio counterparts as to life, uniformity, constancy of characteristic during life and mechanical strength. This feature will allow users of these tubes in industrial equipment to decide the merits of such specialized designs for their own particular devices and operating conditions. The use of this specialized line of tubes for industrial applications as described will be watched with great interest both by the tube makers and manufacturers of industrial electronic equipment.

That this general subject is an important one and of considerable interest to the members of the Institute is evidenced by the fact that the AIEE joint subcommittee on electronic instruments prepared a questionnaire on the subject of tubes in this field, and in response obtained a wealth of information. This was summarized, presented, and discussed at a 2-day meeting sponsored by this committee in Philadelphia during March 1948.

Tube Life

Of all the tube characteristics of interest to industrial users, life[14] is probably the most important. There are a number of features in connection with tube life that are of interest.

If the operating life of a tube were dependent on a pure random phenomenon, then a plot between the percentage of the tubes still operating and hours of operation would look like curve *A* in Figure 8. In such a case, taking a tube out of service, for instance, at 1,000 hours and substituting a new one as a method to insure continuity of operation is illogical. The reason, of course, is that at the end of 1,000 hours any given tube is as likely to operate another 1,000 hours as a renewal tube is to operate its first 1,000 hours.

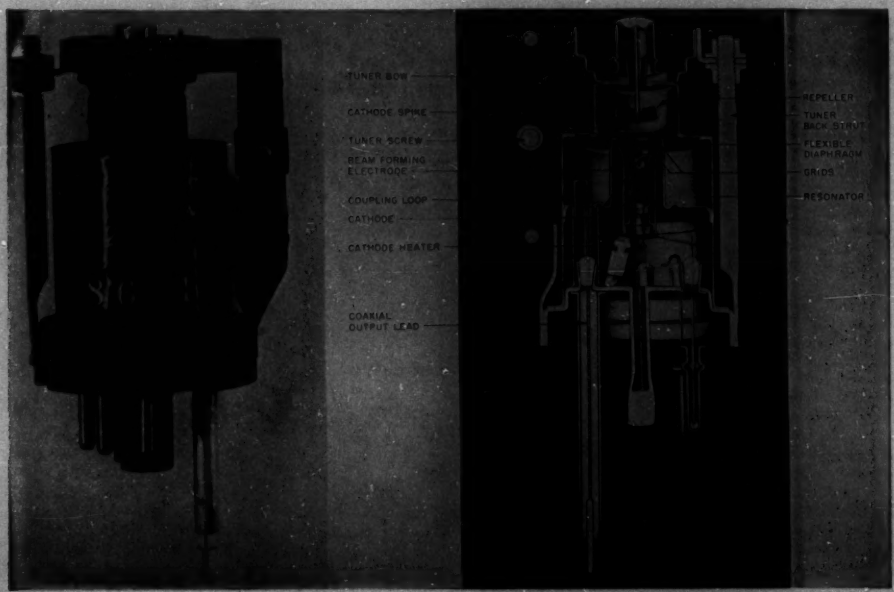
When the life of a tube is based on some definite physical effect, such as evaporation and burnout of a pure tungsten filament, then the life expectancy curve tends to assume a shape, such as shown in curve *B*. The more definitely the end of life is associated with a single definite variable and the better the manufacturing control in the making of the tube, the more pronounced will be the shape of this curve.

The upper left portion of these curves is the important region. This is due to the fact that prematurely early failures are particularly troublesome to the users and, therefore, considered undesirable by tube manufacturers. To a large extent these early failures are due to accidents rather than to any basic factor controlling life. The average life of many modern radio receiving tubes, however, is satisfactory for some industrial uses.

There appear to be two ways of meeting this situation and both are being employed. The first is the introduction of continual improvements in manufacturing control and testing so as to eliminate before shipment the tubes that will show early failure. The second method is to provide increased design life and further

Figure 9. Microwave reflex oscillator tube

This general design suitable for frequencies up to 10,000 megacycles



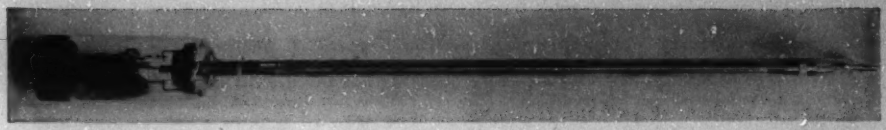


Figure 10. Traveling-wave tube having high gain and very broadband characteristics in the 4,000-megacycle region

insure longer life by conservative operation and provide improved conditions of voltage constancy and a minimum of off-on interruptions. By this method the abscissa scale of Figure 8 is extended to such an extent that the critical right upper portion of the curve tends to become flatter for any definite number of hours.

Small Tubes for the Telephone System

Many of the engineering requirements fundamental in this application of electron tubes are quite different from those of radio broadcasting. For the most part, the tubes are of low power and superficially many appear to be similar to radio receiving tubes although they differ to a marked extent in ability to meet certain basic requirements.

The frequency range of tubes is just as extreme as in radio broadcasting, running all the way from 60-cycle, low-power rectifiers[15] to 4,000-megacycles and above as used for microwave relaying stations.[16]

In the voice and carrier frequency field, uniformity of characteristic, constancy of performance during life, and a long average life free from early failures are all important requirements. Transcontinental lines, particularly if carrier is involved, require a simultaneous use of many hundreds of tubes. This emphasizes the need for life reliability in each individual tube. To gain this end, a long-life design is the first requirement; and this is supplemented by continuous operation and carefully controlled operating conditions such as constancy of cathode heater voltage. In such applications it is also customary to check the tube performance periodically and eliminate tubes in which a slight falling-off of performance is noted. It is sound economically in the telephone system to employ a higher cost for certain tubes in order to obtain better performance and life because of the economies effected elsewhere in the system.

Miniature tubes are of interest for mobile equipment, in satellite installations where the complete equipment is mounted in a small case, sometimes on a pole installation, and where the power supply of filament and plate may be supplied over the incoming lines.

There is not a great deal of interest in steel-envelope tubes except for special cases where the metal envelope of itself is highly desirable. A good example is the single-cavity reflex oscillator tube. A movable metal diaphragm for tuning is a necessary part of the design (Figure 9).[17]

For certain types of equipment, it has been found advantageous to mount the tubes permanently into a chassis and substitute soldered connections to the tube terminals for the usual base and socket arrangement. The completed assembly is treated as a packaged unit, removable as a whole, for inspection, maintenance, or repair. A "permanent" tube designed to be incorporated inside a cable has been reported and in such cases, of course, the tube life must match the estimated useful life of the cable.

At the present time there is much interest in tubes for use in the 4,000-megacycle region which has been assigned by the Federal Communications Commission for microwave beam relaying. The velocity-modulation type of tube, of which the klystron is an example, now is used in the Boston–New York microwave relay link. Showing promise for future installations is the traveling-wave tube (Figure 10) due to its high gain and broad-band characteristics, and the disk-seal triode for its low noise and satisfactory broad-band features. In the highly refined designs of these special microwave tubes, close cathode to grid spacings, fine grid wire diameters, and a smooth surface for the electrode structures are carried to extreme values.

Figure 11. A television picture tube with a 10-inch diameter face

In the case of small thyratrons, there is not the marked trend toward gas filling evident in the industrial field. Inert gas filling is used frequently but only as an addition to and not as a substitute for mercury vapor. This is a logical combination because extreme temperature variations are not often encountered in this field, the anode voltages are low and the tubes run practically continuously.

Television Picture Tubes

Up until recently engineers in this field were most active on the development of projection-type tubes to provide larger pictures than those obtainable directly on the face of the tube. Although there is still activity on this subject, it is not occupying in general as prominent a part in development programs as it did a year ago. This is due probably to the fact that television commercially seems to have "turned the corner" finally. Therefore, all possible effort is being placed on lowering the cost and improving the performance of direct view cathode-ray tubes for television reception. Projection tubes as now used give a picture none too favorable in sharpness and brilliancy when compared with the most modern larger direct view tubes.

There seems to be a growing opinion that until the resolution of the picture is improved increasing its size is only beneficial when it is viewed from a greater distance and is, therefore, chiefly applicable to larger groups of people than normally gather in the average home.

The opinion also is expressed that the projection tube is not now the key factor in the problem of larger pictures by projection. The real problem appears to be a satisfactory low cost optical system to supplement the tube.

Of considerable interest in the industry today is a discussion of two active developments, namely, the matter of alumi-

num-backed screens and ion traps. Many factors are involved. The aluminum-backed screen, the newer feature of these two, has a deposit of very thin aluminum on top of the fluorescent material in the bulb interior. The proponents for this development are enthusiastic about the brighter image obtained, the improved contrast and definition of the picture, and the prevention of the formation of the troublesome ion spot.

The ion trap is a tube design feature aimed to solve in a practical way the problem of the ion spot. This ion spot is a circular spot or area of darkening discoloration on the face of the tube that slowly and increasingly shows up during tube life. It has been found to be caused by the negative ions produced from the electron beam. The ion trap is an ingenious arrangement by which a magnetic field is made to deflect the negative ions into a different path than the electrons on somewhat the same principle as employed in the mass spectrometer. Advocates of the ion trap believe that it is the surest way of eliminating this troublesome effect and at the same time give the lowest tube cost for a satisfactory picture.

The life of direct view cathode-ray tubes as used in television in general has been very satisfactory as is the color of the phosphor and its persistence. Unlike the field of radio receiving tubes, there probably will not be as large or as rapidly growing number of types of picture tubes at least in regard to the bulb dimensions. This is due to the fact that in order to obtain low-cost bulbs the manufacturer must expend very large sums in tooling up for their production.

There is a thorough standardization system similar to receiving tubes set up among manufacturers as regards interchangeability on a given type designation. The present standard enjoying the largest production is a 10-inch diameter tube. However, there appears to be a trend to 12-inch tubes and possibly larger ones if they can be produced at only slightly higher cost than the 10-inch size (Figure 11). Tubes having a face diameter of 20 inches are now commercially available. The matter of rectangular-faced bulbs is receiving some consideration for production.

The beam in a cathode-ray tube may be focussed either by a magnetic or an electric field. The same is true of the deflection of the beam. Many factors are involved in the choice made of these methods. Today the majority of the present standard 10-inch picture tubes use a magnetic field for each function.

During the past few years there has been an improvement in the appearance of the television picture as a result of the flattening of the face of the bulb. This trend seems to have reached a certain degree of stability using a bulb face with a relatively large radius of curvature rather than a real flat surface.

The television receiver designers of course, would like shorter cathode-ray tubes to aid them in building smaller, lower cost cabinet designs. Today, however, there is no apparent way of accomplishing this to a marked extent without introducing other features that are disadvantageous and tend to offset the gain of shortened length.

Transmitting Tubes

General

Unlike radio receiving tubes which, as a class, are highly standardized through the agency of the Radio Manufacturers Association, there is little such standardization in the field of radio transmitting tubes. In general, each make of transmitter requires the use of tubes specifically designed for it by one particular manufacturer. This probably makes obsolescence and development a more rapid process but, from the viewpoint of a user or purchaser of the equipment, it would appear to have some disadvantages. Type number designations, however, are assigned by a joint agency of the National Electrical Manufacturers Association, and the Radio Manufacturers Association. Formerly the system was a combination of numerals and letters, such as *9C24*, but the newer procedure on other than receiving and cathode-ray tubes is the assignment of a 4-number designation, such as *5518*, in which the numbers have no significance in regards to the size or characteristics of the tube. The custom of prefixing the type number by a manufacturer's letter identification continues.

One general trend in new designs is the increasing use of thoriated filaments[18] (except in the case of magnetrons) rela-

Figure 12 (left). A high-power transmitting tube for radio broadcasting

This air-cooled unit will dissipate 20 kw and weighs over 200 pounds

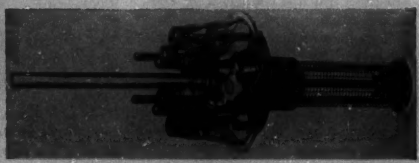


Figure 13. Moulded glass terminal header for a high-power tube

tive to pure tungsten and coated filaments. The so-called thoria-coated filaments, as distinct from thoriated filaments, are in limited use but it is doubtful whether this type of cathode will become commonplace.

Another general trend is toward forced air cooling for larger and larger sizes of tubes (Figure 12). Per kilowatt of output, an air-cooled tube is more costly than a water-cooled tube, but the manufacturers and users of radio transmitting equipment, particularly in the broadcasting field, greatly prefer air-cooled tubes.

Probably one of the most active, forward-looking laboratory developments not yet in production is the use of ceramics[13] for seals and terminal insulators to replace glass. This new material is of great interest because certain compositions have very low dielectric loss for the high frequencies at high temperatures. It also can operate at much higher temperatures than glass without softening or being subject to electrolysis. Another advantage is that it provides opportunities for new assembly procedures. As mentioned previously in this paper, this development is one of the two main contributions from enemy countries during World War II. In the case of ceramics, the idea that they could be used for this purpose is the chief contribution as the materials we are using in the United States and the methods of sealing them to other parts of the tube structure are being developed along very different lines than in Germany.

Some General Problems in This Field

When asked about ever-present problems, one engineer expressed it rather aptly by saying that one of the most troublesome ones was "getting more emission where you want it and less where you don't want it." In other words, more emission, particularly per unit of area, is constantly desired for cathodes, and at the same time grid emission must be minimized.

The designers of transmitting tubes also are being reminded constantly that designs are needed for ever-higher frequencies and once such a design has been produced, then the demand is for ever-larger and larger power ratings.

From the structural viewpoint, there is a constant urge toward closer interelectrode spacing and finer grids in triodes and tetrodes. This urge, of course, comes from the need for higher values of transconductance at low output impedance in order to get improved broadband operation particularly in frequency modulation and television.

One noticeable design feature in many of the newer tubes is the localized cooling of both filament and grid leads as well as certain areas of glass bulbs or seals subject to local heating. This is accomplished by both sharply directed air streams as well as water circulated through cooling ducts. The need for such cooling well might be eliminated by the use of ceramic materials.

Some Materials and Techniques

In large glass transmitting tubes, nearly all of the newer designs utilize moulded glass headers, the terminal pins all extending through this header (Figure 13). More recently, and particularly for the shorter wavelengths, the glass header is being replaced by one of metal and bushing type seals or their equivalent are being introduced (Figure 14). Thus in one way or another, there is a tendency toward eliminating glass, in so far as possible, in all tubes for very high power, particularly those for the shorter wavelengths.

In all except the smaller sizes of tubes, seals utilizing large wires through a pinched-off stem are being replaced by disks, cups or cylinders of alloy metals.

The metal zirconium is being used to an increasing extent as a coating on hot radiation-cooled electrode surfaces. It acts as a getter and also forms a surface having greater heat radiating properties than a shiny metal It is employed on a number of different base metals. For new designs having radiation-cooled anodes, graphite and pure tantalum do not seem to be employed as extensively as a few years ago.

For the ever-present grid emission problem, several methods are being worked on intensively but covering the surface with some form of platinum seems to be a favorite method.

Classification by Frequency

A convenient way to cover the subject of transmitting tubes is to group them into several classes based on broad frequency ranges:

1. Broadcast frequencies up to several megacycles.
2. Frequencies used in frequency modulation and television transmitters. This now extends up to 216 megacycles.
3. Frequencies from the previous value up to about 1,000 megacycles. This includes

Figure 14. A twin power tetrode for inputs up to 10 kw and for frequencies up to 300 megacycles

Figure 15. Forced air-cooled tetrode, incorporating seals and terminals

For outputs up to 1.5 kw at 110 megacycles. Designed for grounded-grid circuits

Figure 16. Water and forced air-cooled triode for outputs up to 6.4 kw at 220 megacycles

Designed for grounded-grid circuits

Figure 17. A small transmitting tube incorporating a ring-type grid terminal and suitable for use in a grounded-grid circuit up to 1,200 megacycles

(Over-all length about $3^1/_2$ inches)

the so-called citizens' band and the very large number of channels set aside for improved television utilizing either high definition of color or both.

4. Frequencies above 1,000 megacycles and commonly designated as microwaves.

Present Broadcast Range. There is not a great deal of activity in this field, particularly in the smaller sizes of tubes where requirements are pretty well taken care of by present designs and war stock surpluses are also a factor.

The feature of utilizing 2- or 3-phase filaments to minimize hum, which was common a few years ago, is not appearing in the newer designs. This is due partly to better hum compensation or balancing methods as well as the fact that the problem is less acute due to the lower currents utilized in thoriated filaments.

Frequency-Modulation and Television. This field is very active in requiring new designs (Figure 14) up to those large enough for a station rated at 50-kw output. One of the chief problems confronting the engineer in this field is that of obtaining more electron emission per unit area of the cathode. This factor has been improved over tenfold during the past few years but the urge to improve it still further continues. Many of the new types incorporate disk or ring-seal features for a grid terminal and are designed for use in grounded-grid circuits (Figures 15 and 16).

As has been noted, forced air cooling is favored by the users of broadcast transmitting tubes. In the past not too much attention has been given to the aerodynamics involved but this factor probably will be studied more thoroughly and marked improvements made in the newer designs.

The power gain per stage is an important factor but is tied up in a rather complicated way with several other factors.

Some engineers feel that magnetron developments may to a marked extent affect future activities in transmitting tubes for these frequencies. If such magnetrons can be designed with satisfactory output and efficiency and capable of modulation with low power, such a combination may well change the tube design picture. This is particularly true if the result is suitable for both frequency or amplitude modulation. Considerable research work in this field is going on.

Frequencies up to 1,000 Megacycles. There is not much activity in developing special tubes for the so-called "citizens' radio band" (460–470 megacycles). There seems to be a general feeling that this application might find satisfactory use on a small scale but in mass quantity use there are many difficulties. This has resulted in a "let's wait and see" attitude. There are tubes available for this application that will take care of present developments although that is hardly true for light portable transmitting equipment.

For the broad band of frequencies between 480 and 920 megacycles assigned to television developments, there is also considerable activity, but the situation is similar to that in the present television band. In other words, triodes and tetrodes utilizing certain features of disk-seal tubes are applicable in some cases (Figure 17) although the types of tubes suitable for the next higher range of frequencies are not being forgotten.

Ultrahigh-Frequency Tubes. At the present time, except for high-frequency heating, most of the applications, actual and proposed, are in the field of communication. The power ratings of such tubes are also relatively low although this is more a matter of inability to make high-power tubes except as oscillators.

There are a number of varieties of tube types which are useful in this frequency range. It is too early for a clear or final analysis indicating the position of each type and to where and what extent each will be used. The following comments seem to cover present engineering opinions:

Magnetrons[50]—These tubes are primarily oscillators not usable as amplifiers but are suitable particularly where high peak powers are required, such as radar. They are suitable also for generating continuous output high frequency and, as has been indicated, well might fit in the frequency modulation and television picture if satisfactory means of modulation can be developed. They appear also to have a very promising future in high-frequency heating at about 1,000 megacycles and above (Figure 18).

Velocity-Modulation Tubes[51]—These tubes are also designated as velocity-variation tubes and klystrons (Figure 19). They are useful as oscillators and amplifiers and lend themselves very readily to frequency modulation. They have proved to be exceedingly useful as signal generators and as power oscillators within the range of their ratings. The chief disadvantages are their relatively narrow bandwidth as amplifiers, low efficiency and relatively high level of inherent noise that limits their use as receiver amplifiers.

Traveling-Wave Tubes[4]—These tubes (Figure 10) have extraordinary capabilities as regards high gain in combination with an exceedingly broadband for amplification. They appear to be subject to somewhat the same limitations as to noise found in connection with klystron tubes. This tube is a relatively new development and accurate appraisal is not yet possible.

Disk-Seal Triodes and Tetrodes[52]—These now are limited in frequency to about 4,000 megacycles and have relatively low gains as amplifiers. Control of frequency and possibilities of satisfactory modulation are not

considered as good as in the klystron. Their outstanding advantage is their low signal-to-noise ratio making them suitable for low signal level amplification. Their capabilities as broad-band amplifiers are not as outstanding as the traveling-wave tube but are apparently sufficient for many applications.

Duotrons—These tubes are structurally similar to a triode but operate with a moderately noncritical transit angle and require a radio-frequency short circuit between cathode and grid. Since the grid and cathode have no radio-frequency potential difference, only one tuned circuit is required. They are the newest of the special types listed and very little information is yet available. They would appear to be particularly promising in fields that are now served by the klystron and disk-seal types.

Thyratrons and Ignitrons for Industrial Control

Thyratrons

Here the most pronounced trend is the increased use of an inert gas filling in place of mercury vapor. The object of this change is to make the operation of the tube and its characteristics independent of ambient temperature over a very wide range. The variation of operation with temperature often presents difficulty in the application of mercury-vapor tubes due to the great change in vapor pressure with change in temperature of the mercury. Indirectly the elimination of the mercury vapor helps to shorten the warming-up time. This independence of ambient temperature is also very helpful in fitting in with the modern trend toward totally enclosing industrial control equipment in steel cabinets. The inert gas used is pretty generally xenon. The real problem in this substitution of an inert gas has been to prevent the "clean-up" of the gas which resulted in short life. The attack on this problem has been along two lines, the first involving circuit arrangements[33] to lower the rate of change of inverse anode voltage on the tubes. The second improvement lies in the design and processing of the tubes themselves.[34]

Much progress has been made in both these directions. The circuit in which the tube is used is still a factor but less vital than it was a year or two ago. Tube designers are quite confident that in the not too distant future they will be able to build gas-filled tubes giving an entirely satisfactory life under almost all required circuit conditions within the rating of the tube.

As a part of this program of improved tube design, the deionization time[35] has been decreased. This also permits tubes of the new design to be used at frequencies higher than 60 cycles. This is a favorable condition toward possible general application on the 400-cycle power supplies used in some aircraft and for other special conditions.[36]

Thyratrons are now available in miniature dimensions for control and instrumentation applications where the average current requirement is in the milliampere range. Quick heating of the cathodes (one minute or less) is considered a very desirable characteristic by the users of thyratrons. This feature is increasing the use of filamentary cathodes rather than heater cathodes in many of the newer designs.

Thyratrons are made in both metal and glass envelopes. Industrial users of this class of tubes tend to favor the metal types on the basis of their being sturdier but cost is the real determining factor in the choice of envelopes. At the present time in considering new designs, an average current rating of about ten amperes is the dividing line. For a rating smaller than this, glass envelopes are indicated, and for higher ratings, steel envelopes.

A 25-ampere average current rating is at present about the largest size of thyratron commercially available. For tube current ratings above that value, ignitrons are favored. Thyratrons are being applied more frequently at higher voltages. An example is in the power supply for high-frequency heating equipments.[3] Quite a number of years ago, the shield-grid design of thyratron[38] was brought out as a distinct type. Now most new thyratron designs have incorporated in their structure what amounts to a shield electrode. Their classification as shield-grid thyratrons, however, depends upon whether the electrical connection to this shield is part of the cathode structure or whether it is brought out to a separate base terminal. In the latter case they are distinctly in the class of shield-grid tubes (Figure 20). In general, this feature of an independent connection to the shield is more common in the small tubes used entirely for control operations than in the larger tubes where power conversion or power output is usually the predominate use.

Air cooling of thyratrons rather than water cooling is practically universal.

Sealed Ignitrons for Control Applications

There has not been a great deal of change in design of these tubes for a number of years nor in general have any new types of larger or smaller ratings been added. The present available types seem to be meeting the requirements in a satisfactory manner. Use of steel en-

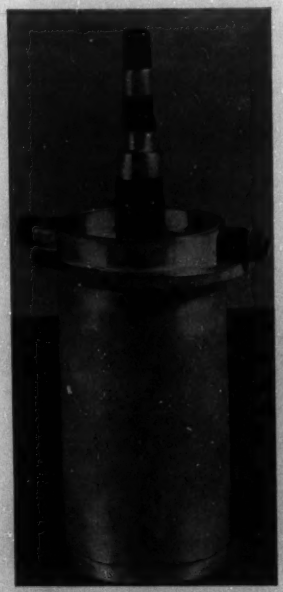


Figure 18. A 5-kw water-cooled magnetron designed for high-frequency heating at 915 megacycles

(Over-all length about 9 1/2 inches)

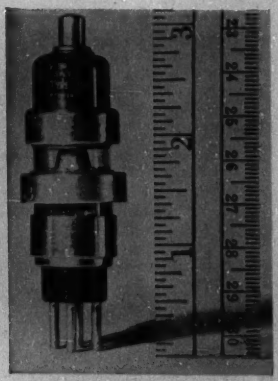


Figure 19. A klystron with frequency range of 2,000 to 4,000 megacycles

Figure 20. Shield-grid thyratron for industrial use

velopes is almost universal for ignitrons and all except the smallest available sizes are designed for water cooling.

Ignitrons for welding control are available and their application economically sound for use at much lower average current ratings per anode than in the case of ignitrons for power conversion. This is due to the fact that the peak current rating for the tube is the important factor in welding control rather than the average current. An ignitron with an average current rating of only about 12 amperes has a peak current rating as high as about 1,000 amperes for some duty cycles. Thus we may have the rather surprising combination of a 12-ampere average ignitron requiring a thyratron to fire it that has a peak current rating considerably higher than this figure.

The sealed type of ignitron has supplanted completely the pumped type in the welding control field.

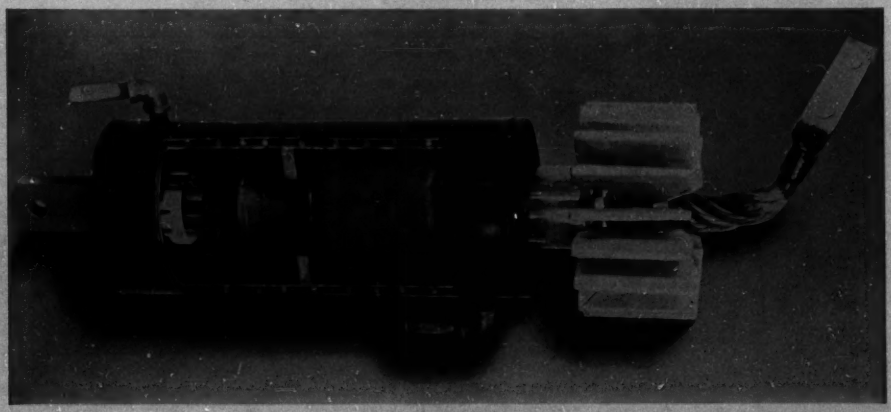
A number of years ago, it was hoped that a pair of ignitrons connected "back to back" constituted an almost universal form of a-c switch or contactor or even a regulator having unusual characteristics that would find many other applications in addition to welding control. To date this hope has not materialized. The most promising new application is in connection with the operation of large d-c motors from an a-c supply line.[29]

Sealed Ignitrons for Power Conversion

One of the most interesting trends in tubes of this class is the marked tendency toward the use of larger sizes. In other words, the line of demarkation between the sealed units and the pumped units is rising to larger and larger ampere ratings. Last year sealed tubes rated at 400 amperes per anode were introduced[30] and now are finding ready acceptance. This represents about a doubling of the amperes per anode over the previous largest size. Another doubling of this factor and a great many of the applications now served by pumped tubes can be handled by sealed-off types.

The single anode type of tube (Figure 21) that always has characterized the ignitron was adopted for the pumped type of rectifier a number of years ago and now almost has superseded, at least in the United States, the multianode unit. In England and in Europe, however, multi-anode tubes are still in the majority and what to American engineers appears to be huge glass bulbs are in common use over there. There has been a decided trend also in the past few years to operate ignitrons at higher voltages[31] than the 250 and 500-volt d-c ratings that formerly comprised the majority of applications. They are now being built and used in connection with power conversion equipments up to 20 kv[32] (Figure 2). They also are finding their place in the power supplies for high-power radio transmitters.[33,34] The sealed ignitron mercury pool tube has so far failed to find application in the lower current ratings. This is not directly due to any design feature in the tube itself but rather to the fact that the cost of ignitron firing circuits is high and is almost a constant figure regardless of the output rating of the ignitron that it controls. Therefore, as the ratings of a rectifier become smaller and smaller, the expense of the firing circuits does not decrease, and finally the cost of this auxiliary is so high in comparison with the rest of the rectifier that the over-all result is not economically sound. At the present time this lower limit for an economical design of sealed pool tube rectifiers is at about 100 amperes per anode. This situation represents one of the problems confronting engineers

Figure 21. Cut-away view of a water-cooled typical sealed steel-envelope ignitron as used for power conversion



active in the development of tubes of this general type for wider applications.

Considerable thought and effort, therefore, are being given to the development of ignitors that require much less current than the present ones that take a maximum of about 25 amperes.[35] This is far from an easy problem in view of the outstanding performance of present-day ignitors as regards life and reliability. Life of a modern ignitron is measured in years rather than in a few thousand hours and it naturally requires considerable time to develop new devices with the assurance that these long life records will be maintained.

Most large steel envelope sealed ignitrons contain two separate ignitors. In view of the long average life obtained in modern tubes the question has been raised as to whether two ignitors are really necessary or advantageous. The concensus of opinion seems to be that two ignitors will be continued as a design feature. They represent low-cost accident assurance against ignitor breakage or burn-out resulting from trouble in the firing circuit. Designers of these tubes point out, however, that when a tube finally fails after many years of satisfactory service it is usually from some other cause than ignitor failure and, therefore, it is seldom that the spare ignitor can be used to rejuvenate completely the tube and start it off on a second career of long life.

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No Discussion

Change of Frequency on the System of the Southern California Edison Company

H. W. TICE
ASSOCIATE AIEE

A. A. KRONEBERG
MEMBER AIEE

W. N. JOHNSON
MEMBER AIEE

J. D. ENEFER
ASSOCIATE AIEE

E. E. TUGBY
ASSOCIATE AIEE

C. L. SIDWAY
MEMBER AIEE

THE scheduled completion on October 26, 1948, of the change in frequency from 50 to 60 cycles of the Southern California Edison Company system and its customers' frequency-sensitive utilization equipment will bring to a close the largest program of its kind ever undertaken in the history of the electrical industry.

The frequency of 50 cycles was established in southern California in the year 1893 when a group of pioneer utility men placed into operation the first 3-phase hydroelectric generating units for commercial use in the United States, and it is believed, in the world. These units, consisting of two 250-kw 2,200-volt generators, were installed for local use near Redlands, Cal., a distance of approximately 75 miles from Los Angeles. This early installation was the beginning of the development of a number of small and isolated systems throughout southern California, all of which developed at 50 cycles, and later merged with, or were acquired by, the Southern California Edison Company.

The decision of this pioneer group to use a frequency of 50 cycles was based upon the economics of generating and transmitting electric energy for lighting and power purposes where the load centers were remote from the source of generation, and long distances of transmission were involved.

At the time these early installations were made in southern California, there had been no standard established for the frequency of a-c generation, as many of the utilities throughout the country were experimenting with frequencies ranging from 25 to 133 cycles, depending upon the class of service rendered. It was not until after the turn of the 20th century that frequencies tended to standardize at 60 cycles in the United States, while in Europe and most of the rest of the world, frequencies were standardized at 50 cycles.

During these early developments in southern California by the privately-owned utilities, a number of municipally-owned systems were established also in and around Los Angeles, principally in the cities of Burbank, Glendale, Los Angeles, and Pasadena, all of which developed at 50 cycles. As a result, the major portion of the area in southern California, with the exception of the San Diego Gas and Electric Company, the territory served by the California Electric Power Company, and a small portion of the city of Los Angeles served by the Los Angeles Gas and Electric Corporation, developed at 50 cycles.

With the advent of Hoover Dam power and the acquisition by the Department of Water and Power of the City of Los Angeles of the properties of the Los Angeles Gas and Electric Corporation in 1936, the Department of Water and Power changed the frequency of its system to 60 cycles. This also necessitated the cities of Burbank, Glendale, and Pasadena to likewise change their systems to 60 cycles as the Department of Water and Power is the generating and transmitting agent for Hoover Dam power to these cities.

Since the conversion of these cities to 60 cycles did not materially affect the company's operations, it was not until the war was well advanced that the company gave consideration to the change of its system to the higher frequency. This consideration was brought about because of the large amount of frequency-sensitive items of equipment installed during the war for which the company's customers were required to pay premium prices for 50-cycle equipment, and it appeared this price situation would continue post-war.

On June 8, 1945, the executive committee of the board of directors authorized filing an application with the Public Utilities Commission of the state of California for authority to change the frequency of its system from 50 to 60 cycles. Following a public hearing, the commission, on July 27, 1945, issued its order directing the company to proceed with the establishment of a uniform system-wide a-c frequency of 60 cycles and to cease rendering 50-cycle service at such time as it was in a position to render 60-cycle service in lieu thereof. The commission's order further provided that the company assume, in addition to the conversion costs of its own equipment, the reasonable direct costs of adapting customers' appliances and equipment for proper operation at a frequency of 60 cycles.

The cost for the conversion of the company's system and its customers' utilization equipment is now currently estimated at $35,000,000, of which approximately $33,000,000 is chargeable to operations and the balance to plant.

Scope and Size of Conversion Program

From the early beginning of the company's system in 1893, the company ex-

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H. W. TICE is vice president in charge of operation and A. A. KRONEBERG is senior electrical engineer, both of the Southern California Edison Company, Los Angeles, Cal. W. N. JOHNSON is manager; J. D. ENEFER is assistant manager; E. E. TUGBY, planning engineer and C. L. SIDWAY, principal engineer, all with the frequency change department of the Southern California Edison Company.

Figure 1. Transmission system of Southern California Edison Company

panded in less than 55 years to become the fifth largest privately-owned electric utility in the United States in kilowatt-hours generated. The service area of the company covers approximately 18,000 square miles with a system power demand in excess of 1,000,000 kilowatts with an annual generation of approximately 6,000,000,000 kilowatt-hours. The company's system serves directly some 75 incorporated cities and 150 unincorporated communities and outlying rural territories. It also supplies energy for redistribution to four municipalities at 50 cycles and to three privately-owned utilities on a wholesale basis at 60 cycles. The territory served includes large areas devoted to farming and fruit growing, much of which is made possible by irrigation. Among the more important industries served are the production and refining of oil, production of motion pictures, fruit packing, the manufacture of cement, rubber tires, chemicals, airplanes, oil well equipment, automobiles, and the production and fabrication of iron and steel. In addition, the company serves two electric railways, the Pacific Electric Railway Company operating a system of interurban electric lines in the vicinity of Los Angeles, and the Los Angeles Transit Lines operating electric streetcar lines and trolley busses serving the city of Los Angeles.

Figure 1 shows the territory served by the company and its major transmission system and generating plants.

The number of customers served by the company as of December 31, 1947, totalled 765,000, of which approximately 700,000 were served from the 50-cycle system now under conversion, and the balance at 60 cycles. The 60-cycle service area is located in the San Joaquin Valley where the company acquired the 60-cycle properties of the Mount Whitney Power and Electric Company in 1920 and has since continued serving this area at this frequency.

The foregoing briefly outlines the development of the Southern California Edison Company into a single integrated system serving a highly developed agricultural and industrial area, and is set forth so that the reader may have some idea as to how and why the company's system developed at 50 cycles, and the magnitude of the work involved in converting its system to the now generally accepted United States standard frequency of 60 cycles.

Physical conversion of the company's system commenced September 6, 1946, and it is estimated that, as of June 1, 1948, the company will have completed the conversion of 525,000 customers, representing 75 per cent of the total, and 1,600,000 horsepower of connected load, representing 78 per cent of the total project.

The conversion is being made concurrently with an unprecedented load growth in the territory served by the company which requires the planning and construction of new generating and new step-down substations. The 1946–1947 season was one of subnormal precipitation on watersheds of the company's hydroelectric developments and the 1947–1948 season to date is even more deficient in this respect. Operation at two frequencies and outages of steam-electric units for rebuilding tremendously complicate the problem of dry-year operation. Deferred maintenance and repair work that accumulated during war-time operations add their share to the complications of the system conversion. In spite of these complications, however, it is expected that the change of the company's frequency from 50 to 60 cycles will be completed on schedule in October, 1948.

System Engineering and Operation*

Preliminary engineering of the frequency conversion included investigations of existing equipment and its ability to operate at the higher frequency. The following are fundamental relationships involved in studies of frequency conversion:

1. The frequency of a generator is proportional to the number of poles and to the speed of the rotor.
2. The voltage is proportional to the flux and to the frequency.
3. The reactance ohms are proportional to the frequency.
4. The maximum power input to an inductive circuit with a variable resistance load is proportional to the square of the applied voltage and inversely proportional to the frequency.
5. The centrifugal stresses in rotating parts are proportional to the square of the speed, that is, to the square of the frequency.

Prime Movers

The investigation was begun with a study of prime movers and their performance at variable speed. Nearly all prime movers of generating units on the system of the Southern California Edison Company are turbines. Typical characteristics of torque versus speed and power output versus speed of a turbine are illustrated in Figure 2. Normal speed and 120 per cent speed are indicated on the curves. It may be noted that a higher than normal speed the power output decreases.

The input to an impulse turbine or water wheel is independent of its speed, hence the reduction of output at higher than normal speed is a loss of efficiency. This loss of efficiency results from buckets leaving the line of action of the jet before every bit of water completes its work upon the buckets, and some of the water is therefore not utilized. Where this loss of efficiency is of sufficient magnitude to justify the cost of rebuilding, the wheel diameter is reduced and new buckets of altered design are installed. The company is rebuilding wheels at Big Creek 2-A powerhouse where they are driving two 45,000-kw generators and at Kern River number 1 powerhouse, where four 5,000-kw generators are in service. Some 14 water wheels out of a total of 54 are scheduled for rebuilding.

In the radial-flow type of turbine the water is subjected to centrifugal forces. The centrifugal force opposes the flow of water in the case of the inward-flow turbine so that, as the speed increases under constant head, the discharge tends to decrease. Thus, with a reduction of both input and output, the efficiency of the turbine may change little or not at all. In turbines operating under conditions of low head and large quantity of water, the increase of speed may introduce or intensify cavitation. The company is equipping the three 50-cycle units at Hoover Dam with new runners of smaller diameter and with fillers to fit them into the existing stationary parts. The other 12 turbines on the company's system require no major changes for operation at 60 cycles.

Steam turbines are subject to additional

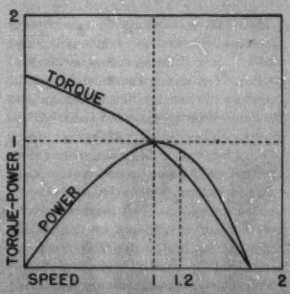


Figure 2. Typical output characteristics of a turbine

* H. W. Tice and A. A. Kroneberg.

complications when operated at the higher speed. The elastic limit of materials is reduced at the very high temperatures to which turbine blades are subjected. The increased centrifugal forces at the higher speed may exceed the elastic limit and may cause a permanent elongation of the blades (creep). A steam turbine rotor is a complex structure of disks and blades all having their natural periods of vibration. The rotor is "de-tuned" for the normal design speed, that is the disks and blades are designed for natural periods of vibration other than normal speed. The critical speed of the precision-built, highly efficient turbines is too near the 120 per cent normal speed to permit safe operation at the latter speed. Of the eleven steam-electric units operating on the company's system, three are old and relatively inefficient vertical units that cannot be opeated at the higher speed. Units *4* to *8R*, inclusive (5), are 60-cycle units originally adapted for 50-cycle operation or installed to replace 50-cycle units removed during the war. The remaining three units are the largest and latest of the 50-cycle units installed on the system. Their operating capacities are 60,000 kw; 100,000 kw; and 100,000 kw. Parts for rebuilding turbines have been purchased for these three units.

There are five diesel-electric units in operation in a leased plant connected to the company lines. These units are rated approximately 5,000 kw each. The engines were required to be operated as near to their design speed as possible. The generators were rebuilt from 50-cycle 36-pole machines to 60-cycle 42-pole machines. The operating speeds are 166.7 and 171.5 rpm, respectively. This resulted in an increase of only 3 per cent in the speed of the diesel engines.

Synchronous Machines

Currents in windings of synchronous machines (generators, condensers, and motors) have limiting values established by heat produced in the resistance of the windings. These limiting values are held unchanged for operation of the machines at the higher speed. However, maintaining field currents at the same values at either speed means operation at constant flux and the varying of voltage in proportion to the speed. This increase in voltage, if it can be sustained on the existing insulation, is highly desirable from the standpoint of over-all system performance. Ohmic reactances are expressed as percentages or per unit values for use in system-wide studies on a given kilovolt-

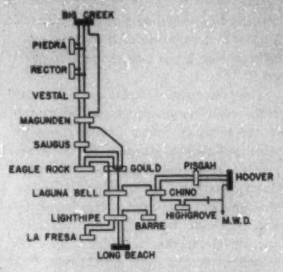


Figure 3. Schematic diagram of the 220-kv backbone of the system

ampere base. Those values represent the ratio of the IX drop to the rated circuit voltage E. Since X varies with the frequency and I for a given base varies inversely as the voltage E, it is apparent that the per unit value of reactance varies directly as the frequency and inversely as the square of the voltage. Low reactance is the prime factor in the steady-state stability of a power system, and is second only to high speed of fault clearing in the transient stability of the system. The maximum increase of voltage on synchronous machines permitted by insulation levels and by available transformer ratio taps is being made at the time of change to 60-cycle operation.

Another important factor in the transient stability of a power system is the inertia constant of a synchronous machine derived from its WR^2 and speed. It may be defined as the time required for the machine to decelerate from full speed to a complete stop with rated torque acting through the entire period of time. With full speed increased to 6/5 of the original speed, WR^2 unchanged, and rated torque reduced to 5/6 of the original torque, the time to come to a stop or the inertia constant is increased to 144 per cent of the 50-cycle value. Further analysis of the inertia constant and its effect on machine performance reveals that it takes 20 per cent longer for a machine to reach a displacement in phase angle of the same number of electrical degrees at 60 cycles than it does at 50 cycles. The effect of increased frequency upon inertia constants of synchronous machines is, therefore, inherently stabilizing and beneficial to the power systems.

The shafts and rotors of synchronous machines are subjected to mechanical stresses and torques. The torques are decreased with increased speed of operation. Centrifugal forces, however, increase as the square of the speed. The rotor structures of rotating machines must therefore be checked for factors of safety used in their design and strengthened or replaced if factors of safety are not deemed to be adequate. Particular attention is paid to stresses in dovetails and in pole tips of salient-pole synchronous machines. Water-wheel generators are designed to withstand runaway speed which is nearly double the normal speed. Operation of water-wheel generators at 120 per cent rated speed is therefore feasible and safe. Steam turbogenerators are not expected to be subjected to speeds in excess of 110 per cent normal because of prime mover design limitations. In view of the importance and the size of the three steam-electric units for which turbine parts have been purchased, they are also being equipped with new generator rotors.

Synchronous condensers are required by AIEE standards to be able to withstand a 25 per cent overspeed. A study of some 19 50-cycle machines on the company's system disclosed factors of safety ranging from 2.5 to 6.0. These factors of safety are reduced to 70 per cent of their original value when the machines are operated at the 60-cycle speeds. Proper weight was given in the study to the age and condition of machines, and to the possibility of obtaining increased capacity economically through complete rebuilding.

The company's program includes the complete rebuild of two 30,000-kva condensers to increased capacity of 45,000 kva each, the purchase of new rotors for five synchronous condensers, and the purchase of sets of new poles for rotors of four synchronous condensers and two machines comprising a frequency changer. The 60-cycle machine of this frequency changer required new poles because it was not designed for starting and a starting winding was necessary for operation as a condenser upon completion of frequency conversion.

The bearing losses in synchronous machines increase as approximately the 3/2 power of speed. In ring oiled bearings, more oil is carried by the rings at higher speed to cool the bearings. Experience of other utilities and that of the company indicates that performance of bearings at the higher speed is unpredictable, but in general, satisfactory and occasionally even improved over original performance. Direct connected and motor driven exciters operating with voltage regulators are expected to give satisfactory performance at 60-cycle speeds. Necessary reinforcing of commutators or notching of pole pieces are details left for determination by tests and detailed engineering.

Transformers and Lines

It is intended to operate power and distribution transformers on the company's system at the same voltage at 60 cycles as they were operated at 50 cycles. The voltages are determined by insulation levels of transformers, busses, transmission and distribution lines, and other equipment. Since voltage is proportional to frequency and to flux, an increase in frequency will result in a reduction of flux and flux density. A reduction in flux in turn means reduced magnetizing current and reduced hysteresis loss. No detectable changes are expected in copper losses and eddy current losses as a result of increased frequency. The reduction in core loss permits an increase of voltage to 107 per cent and a corresponding increase in rated kilovolt-amperes. Similarly, if voltage remains unchanged, the load current may be increased to 103 per cent with a corresponding increase in rated kilovolt-amperes. Tests on representative distribution transformers indicate a reduction of core loss at constant voltage of a little less than one watt per kilovolt-ampere of transformer capacity. Transformer reactance will, of course, be increased to 6/5 of the 50-cycle value and there will be a corresponding increase in regulation.

Characteristics of transmission and distribution lines will be adversely affected by the change of frequency. Reactance and susceptance per mile of line increase with the frequency. Both values enter in transmission line calculations in such a manner that, disregarding effects of conductor resistance, it may be said that a 60-cycle transmission line has approximately the same characteristics as a 50-cycle line 20 per cent longer. Stated differently, it means that the change of frequency is equivalent to increasing the distance of transmission. On short lines of low and medium voltage, it is merely an increase in impedance and regulation. On long high voltage lines, it also means increased requirements of kilovolt-ampere capacity for charging the line. The per cent increase of charging kilovolt-ampere is more than the per cent increase of frequency, but less than the per cent increase of the square of the frequency. That is, charging kilovolt-ampere will increase to a value between 120 and 144 per cent of the 50-cycle kilovolt-ampere. The per cent increase of voltage rise along an unloaded line will also be considerable. As an illustration, the voltage rise along a Hoover-Chino 220-kv line is 10 per cent at 50 cycles, and is 15 per cent at 60 cycles. The resistance of aluminum cable steel reinforced conductors in use on the company's high voltage transmission lines will be increased very slightly as a result of skin effect. This increase is approximately 7/10 of one per cent.

System Stability

The term "power system" is applied to the combined interconnection of generators, transformers, transmission lines, synchronous condensers, and so forth. The power system combines characteristics of its component parts into characteristics of its own. The more important of these are the steady state and the transient stabilities. Studies were made to determine the extent to which increased voltage of synchronous machines and the corresponding decrease in their per unit reactances were able to offset the increase in transformer and transmission line reactance. The effects of the frequency conversion upon the steady state stability of the company's system are illustrated by the following two summations of reactances of generators, transformers, lines, and remaining system:

50 cycle: $0.60 + 0.11 + 0.47 + 0.26 = 1.44$
60 cycle: $0.53 + 0.13 + 0.56 + 0.26 = 1.48$

The ratio of the two summations, 0.97, indicates an approximate loss of 3 per cent in steady-state stability resulting from the change of frequency. Studies of transient stability indicate that although transient reactances of the branches of the system show a net increase of approximately 10 per cent, the permissible time of clearing a fault has increased 20 per cent owing to increased inertia constants, the net result being practically the same transient stability within the practical range of fault clearing of 0.2 to 0.5 second.

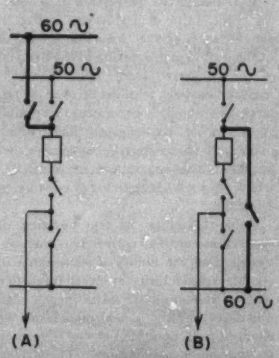


Figure 4. Temporary connection for switching circuits from 50 to 60 cycles

(A) From operating to temporary bus
(B) From operating to transfer bus

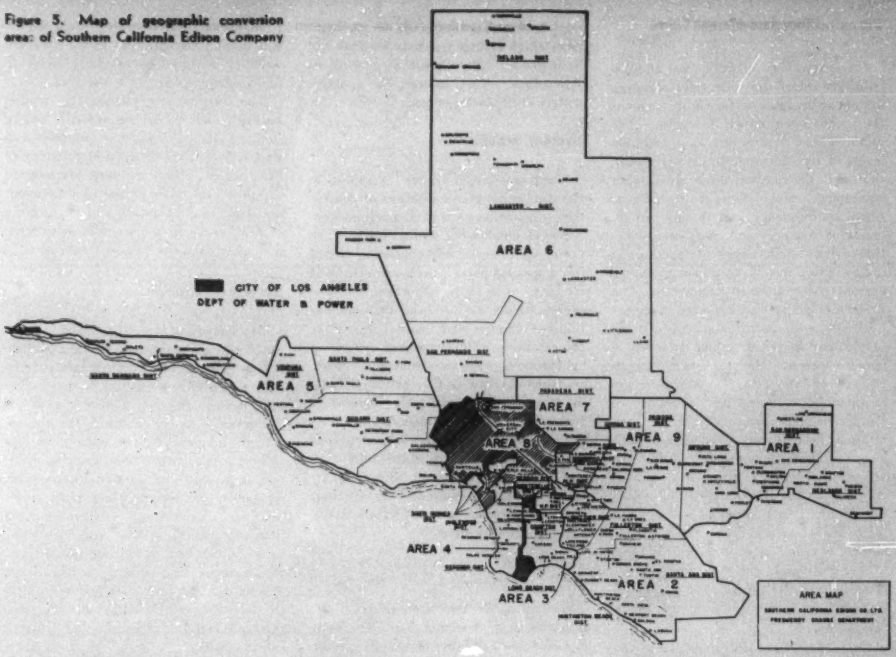
The preliminary engineering studies outlined before determined the major items of change in system equipment and were followed by detailed engineering of the changes. This included preparation of specifications, ordering of equipment and parts, investigation and engineering of changes in auxiliaries and accessories, and scheduling the work of conversion.

System Operation

With equipment to be rebuilt designated by the engineering studies, and the outage times for rebuilding estimated from best available information, it was necessary next to develop a master plan for operation of the system during the conversion of frequency. The conversion, requiring a minimum of two years to carry out, had to be coordinated with system expansion called for by the rapidly growing load. Contractual obligations and limitations of interconnections with adjacent utilities introduced additional complications in the planning of operations. The master plan had to be flexible enough to allow for possible delays in obtaining materials without disruption of conversion schedules.

The company's system may be described as follows: The backbone of the system is a 220-kv three-circuit transmission line shaped like a right angle *L*. The bulk of generation is divided into three almost equal groups. One, Big Creek, a group of hydroelectric generators, is located at the top of the *L* in Fresno county; the second, Hoover, a United States Bureau of Reclamation project on the Colorado River, is connected to the other end of the *L*; and the third, Long Beach, where a group of steam-electric generators are clustered at the apex of the right angle. The distance from Long Beach to either of the hydroelectric groups is approximately 280 miles. Fourteen substations break up the transmission line into 28 sections not counting the line of the Metropolitan Water District of Southern California, which is used jointly by the district and the company. Two of the substations (Pisgah and Gould) are switching stations only; five substations (Piedra, Rector, Vestal, Magunden, and Highgrove) are step-down substations supplying 60-cycle power to the San Joaquin Valley load of the company and to interconnections with other utilities; and seven substations, (Saugus, Eagle Rock, Laguna Bell,

Figure 5. Map of geographic conversion area: of Southern California Edison Company



Lighthipe, La Fresa, Chino, and Barre) are 220/66-kv step-down substations supplying the main part of the company's load.

This load, of approximately 90 per cent of the total, is the 50-cycle load being converted to the standard frequency of 60 cycles. Each of these seven major substations is a source of power for one or two sub-transmission systems of 66-kv transmission lines and step-down substations. There is a total of some 90 such step-down substations involved in the change of frequency, ranging from a simple line tap and a transformer to a switching and step-down substation with a ten-position double bus arrangement. Transformers at these substations step the voltage down to 16.5 kv or 11 kv for high voltage distribution, and to 4 kv or 2.4 kv for primary distribution and for customer distribution systems. There are some 110 substations stepping voltage down from high voltage distribution to 4-kv primary distribution, some 900 substations supplying individual customers and 90,000 line transformers for supplying 700,000 odd customers. Approximately 1,100 distribution and primary circuits in the 50-cycle areas are subject to independent switching at a bus and represent the minimum unit for simultaneous switching from 50- to 60-cycle service.

Main System

In planning the conversion of generating plants to 60 cycles, it was desired to convert each of the three major sources of generation in proportion to the demand of the converted load. This, however, was not entirely feasible because of the large size of some generating units and capacities of transmission lines which made rather large steps of conversion, and because of the long outages for rebuilding which required a staggering of conversion schedules.

Some flexibility in the planning of generation during conversion was obtained from the ability of some units to operate satisfactorily at either frequency with a minimum of adjustments. All units at the Big Creek plants can be changed from operation at one frequency to the other in a short time. The 90,000-kw unit *A-5* at Hoover Dam can be and has been operated at either frequency. The 82,000-kw steam unit *8-R* at Long Beach is equipped with two governors and can be switched from one frequency to the other without bringing it to a stop. Units *4*, *5*, and *6* at Long Beach, totalling 23,000 kw, can be operated at either speed following a shut-down for changes in governor weights.

Additional flexibility in the planning of production was provided by the existence of two 50,000-kw frequency changers at Chino and Highgrove substations. These frequency changers were made up of pairs of synchronous condensers coupled together for this service. Following completion of the program of conversion, they will be uncoupled and placed in service as condensers.

It was originally intended to divide the 220-kv backbone into two systems operating side by side, one at 50 cycles and the other at 60 cycles, the two being interconnected through the two frequency changers.

The company operates high-voltage interconnections with several adjacent 60-cycle utilities. In the north near Big Creek, the interconnection is with the 2,000,000 kilowatt system of the Pacific Gas and Electric Company. In the south, the interconnections are with the Metropolitan Water District and

Arizona utilities, the Department of Water and Power of the City of Los Angeles, the San Diego Gas and Electric Company, and the California Electric Power Company, all adding up to more than a million kilowatts. Delay in obtaining and installing equipment for tie line control made it necessary to divide the backbone into three, rather than two, separate systems. One of these is the remaining 50-cycle system, the second a 60-cycle system identified with the Pacific Gas and Electric Company, and the third, a 60-cycle system identified with the Metropolitan Water District line.

The arrangement of the sub-transmission systems dictated the division of the territory served at 50 cycles into nine conversion areas. Areas numbers 1 and 9 were originally supplied from Chino substation. Area number 1 has been transferred to the newly constructed Highgrove substation at 60 cycles. Areas numbers 5 and 6 are supplied by two transformer banks and two sections of bus at Saugus substation and can be completely separated or paralleled on the bus at Saugus. Other areas are identified with major substations feeding them as follows: number 2—Barre; number 3—Lighthipe; number 4—La Fresa; number 7—Eagle Rock; number 8—Laguna Bell. The sequence of conversion was determined by the needs of the conversion of customer equipment within limitations of the ability to operate the system in such a sequence.

Figure 3 shows a schematic diagram of the 220-kv backbone of the system. The west line from Big Creek to Magunden and the Metropolitan Water District line from Hoover through Highgrove to Chino were operating at 60 cycles prior to the beginning of conversion. The west Big Creek line was extended to Saugus for the conversion of Areas number 5 and 6, then to Laguna Bell for conversion of Area number 8. Area number 1 was, at the same time, converted to 60 cycles and transferred to Highgrove substation. The conversion of area number 2 and of a 100,000-kw unit at Long Beach permitted the linking of all generating sources with a 60-cycle line by switching sections to make one Laguna Bell-Lighthipe-Long Beach line and one Hoover-Pisgah-Chino-Barre-Lighthipe line. After this, the 60-cycle system is gradually increased by transfer of line sections from the 50-cycle system. Tables I and II illustrate load checks made from time to time in order to keep control of loading of transmission lines and of transformer banks. The load checks shown in the tables were made at the half-way mark, that is, at the time when approximately half of the company's load was supplied at each frequency.

Subtransmission System

It is impossible to review in detail all the line arrangements and switching procedures planned and executed for the conversion of the subtransmission systems to 60 cycles, but some of the methods used in the planning of switching facilities and some exceptional installations deserve description. One of the principles of planning was to progress with conversion along the outside of loops formed by 66-kv transmission lines. Nearly all major substations and eight of the step-down substations are equipped with double 66-kv busses with each position connected to each bus through an oil circuit breaker. It is a simple matter of assigning one bus to 60-cycle service and the other to 50-cycle service for the period of operation at two frequencies. Twenty of the 90 step-down substations are simply transformer banks tapped from a line. These substations are usually of low capacity and, with the line, are switched over to 60-cycle service in one operation. Twenty-four substations have single bus or ring bus arrangement of switching facilities. These substations have no more than two line terminals and two banks of transformers to handle, and separation of lines and transformers can be made without temporary connections. An operating bus and a transfer bus arrangement is the prevailing arrangement at the substations of the company, being used at 38 substations for 66-kv bussing and is the standard arrangement at the lower voltages. When the number of positions on such a bus exceed five, it is usually necessary to install temporary connections for operation at two frequencies. The preferred practice is to string a temporary "cutover" bus parallel to the operating bus and to connect to this bus through disconnecting switches all circuit breakers except those scheduled for the last day of conversion.

It is not always possible to make installations of temporary busses at a substation, owing to lack of space and obstructions of various kinds. In such cases, a study is made and some other means devised for accomplishing the necessary switching. A cut in the operating bus at some ten locations permitted the operation of the stations at two frequencies. The transfer bus was used as a "cutover" bus at a number of locations where the time of conversion was of short duration. The diagrams of Figure 4 illustrate temporary connections of a circuit to the temporary "cutover" bus and temporary connections to the transfer bus when the latter is used for conversion.

Among the problems of line and bus switching, two are believed to deserve

Table I. System Load in Megawatts

By Transmission Areas at 6:00 P.M., December 18, 1947

	Net Edison Load			Gross System Load		
	60-Cycle	50-Cycle	Total	60-Cycle	50-Cycle	Total
Piedra-Magunden				98		98
San Joaquin Valley	60		60	60		60
Saugus	85		85	85		85
Eagle Rock*		146	146		146	146
Laguna Bell**	160	40	200	160	40	200
Lighthipe***	85	110	195	85	110	195
La Fresa		147	147		147	147
Barre	56		56	56		56
Chino-Highgrove	50	57	107	107	57	164
Total system	496	500	996	651	500	1,151
Generated and purchased			1,053	699	523	1,222
Loss			57	48	32	71

* Includes Anita, Eaton, Rosemead, Dalton 16 kv load and Beverly.

** Includes Wabash 60-cycle load.

*** Includes Los Alamitos, Carmonita, Somerset, and the El Segundo 60-cycle load.

Table II. System Generation in Megawatts at 6:00 P.M. on December 18, 1947

	60 Cycles	50 Cycles	Total
Big Creek Number 1*	63		63
Big Creek Number 2	55		55
Big Creek Number 2-A		61	61
Big Creek Number 3	62	25	87
Big Creek Number 8		53	53
Base Hydro	35	2	37
Edison Hoover	150	180	330
M. W. D. Hoover	92		92
Long Beach Number 1**	6	44	50
Long Beach Number 2	106	58	164
Long Beach Number 3	109	100	209
Vernon Diesel	17		17
Purchased	4		4
Total System	699	523	1,222
System Capacity	715	587	1,282
Reserve	16	44	60

* Number 3 unit (18 megawatt) out of service for routine overhaul at Big Creek number 1.

** Number 1 unit out of service to clean condensers at Long Beach number 1.

attention. One is the installation of a third 66-kv bus at La Fresa, and the other is the method of conversion of area number 2. La Fresa substation is located in a zone subject to insulator contamination from salt-bearing mist from the Pacific Ocean and fumes from oil refineries and synthetic rubber plants. It has been the practice at this substation to clean insulators supporting the 66-kv busses every four months. This requires alternate outages of the two busses, which is feasible when operating at a single frequency. The substation was scheduled for operation at two frequencies during twelve consecutive months and, it being a source station, no bus outages could have been arranged during the year. For this reason, a temporary 66-kv bus was constructed paralleling the length of the north bus and connections were made to it from 14 lines in a manner similar to connections made to cutover busses at other substations.

At Barre substation, one 75,000-kva transformer bank supplies area number 2, which comprises the entire territory of Orange County. The 60-cycle source of power for area number 2 was established at Chino substation, some 25 miles distant and two 66-kv lines were routed into the area by means of temporary connections at crossover points and by-pass connections at some substations. Approximately half the load in the area was converted to 60 cycles by progressive switching to these lines. At this point, the remaining 50-cycle load was transferred to Lighthipe substation through similar line changes, thus completely relieving Barre substation. The substation was then switched to 60 cycles with the 220-kv line sections and busses. It was again loaded with its 60-cycle load switched back to it from the Chino lines, and the conversion was completed by progressive switching of converted load from Lighthipe to Barre substation.

The method of conversion of Area number 2 illustrates the method commonly used in converting substations with one bank in service when sufficient spare capacity is available at adjacent substations, and when sufficient circuit capacity is available between substations to carry the load. When such capacity is not available, it is necessary to install temporary transformer banks to provide the needed capacity and frequency. Temporary transformer bank installations were required at 16 of the 66-kv step-down substations and at many of the 16.5/4 and 11/4 kv substations. The company purchased two 3,000-kva mobile substations with taps suitable for operation at 16.5/4 or 11/4 kv substations. These mobile units are currently in service as temporary 60-cycle banks at substations under conversion. They are scheduled to remain at various locations from two to five weeks and to be in transit an average of one week.

System operations for frequency conversion are conducted under the direction of a three-man committee. This committee plans the operations in accordance with the requirements of the frequency change department and co-ordinates them with regular system operations. It designs the required temporary connections and programs the switching procedures. The temporary connections are installed by the maintenance and construction crews of the operating department and all the switching is performed under the direction of the system dispatcher.

Conversion of Customers' Equipment*

Organization of Frequency Change Department

Immediately following the order of the Public Utilities Commission to proceed with the conversion of the company's system to the higher frequency, a separate frequency change department was organized to handle all phases of the project pertaining to the conversion of customers' equipment.

This department was organized on the basis of contracting the preponderance of the conversion work to local concerns so as to avoid taking onto the company's payroll a large number of employees whose services would, of necessity, be terminated at the conclusion of the project. Accordingly, five major contracts were awarded. The Bechtel Corporation and the Stone and Webster Engineering Corporation were employed to perform the survey, engineering and conversion of commercial and industrial customers' equipment; the California Electric and Maintenance Company, the survey and adaptation of domestic customers' appliances and equipment; the Fluorescent Maintenance Company, the adaptation of fluorescent and mercury vapor lighting units; and the California Electric Service Stations, the adaptation of electric clocks and timing devices.

The principal functions of the frequency change department are to co-ordinate the activities of the contractors' organizations, and to establish the rules, regulations, and policies covering the manner and extent of changes required to be made in the equipment of its customers. In addition to these functions, the department also handles all customer relation problems, newspaper releases, processing of records, mailing of pamphlets and cutover notices, operation of clock depots for the exchange or cycle-change of clocks, scheduling of the cutover of feeders, review of purchases and subcontracts made by the contractors, review of the engineering determinations and conversion methods employed by the contractors, development of basic engineering changes required to be made in specialized items of equipment, and accounts for the costs of the project.

The only physical work of conversion performed by employees of the department is the changing out, relagging and testing of watthour meters installed on the customer's premises, and the testing and adaptation of deep-well turbine pumps.

The number of employees engaged in frequency change work by the contractors and the frequency change department at the peak of operations approximated 1,777 of which 651 were employed by the department.

Basic Rules, Regulations, and Procedures

In ordering the company to proceed with the conversion of its system to a uniform system-wide frequency of 60 cycles, the Public Utilities Commission required the company to assume, within reason, the direct costs of converting its customers' appliances and equipment for operation at the new frequency, but declared that the adaptation of customers' equipment was a joint undertaking to be assumed by both the company and its customers, and that the company in providing such assistance to its customers must be governed by uniform nondiscriminatory procedures. The commission further required the company to prepare a Frequency Change Manual setting forth the policies, procedures and basic engineering data to be followed in the adaptation of customers' equipment, and to place this manual on file for public inspection in each of the company's district offices.

The following are excerpts from the "Rules and Regulations" as approved by the Public Utilities Commission:

"The company, except as otherwise herein provided, has the responsibility for adapting customers' utilization equipment to render it capable of performing essentially the

* W. N. Johnson, J. D. Enefer, E. E. Tugby, and C. L. Sidway.

same service when supplied from a 60-cycle system as that previously obtained when supplied from a 50-cycle system.

"The company, or its contractors, will make the necessary changes required to adapt the customers' utilization equipment in accordance with the company's engineering determinations. The customer will, without charge, make his utilitization equipment available for adaptation and provide necessary assistance to avoid delays during the progress of the work.

"The cost of additions, betterments, repairs, or maintenance of customers' utilization equipment is not a part of the reasonable cost of adaptation. Any such work shall be performed at the expense of the customer.

"A customer may assume the responsibility of adapting his utilization equipment and in such case the company will make a conversion allowance in lieu of adaptation.

"In exceptional cases or unusual circumstances where utilization equipment cannot be adapted for 60-cycle operation at a reasonable cost because of its condition, age, inadequate, obsolete or nonstandard design, the company will make a conversion allowance in lieu of adaptation.

"Rotating equipment will require adaptation only if it is speed critical and in such mechanical changes will generally be made in the drive or driven equipment. Where mechanical changes are, in the opinion of the company, not practicable, electrical changes will be performed.

"The company's responsibility for the adaptation of the customers' utilization equipment shall cease and terminate 60 days after the date of cutover, except as otherwise provided in this rule and regulation.

"In the event any customer's equipment is damaged, as a result of the conversion to a frequency of 60 cycles, the company and its contractors will be liable only if such damage is due to the negligence of the company, or its contractors. Such liability shall be limited to the repair of the damaged equipment.

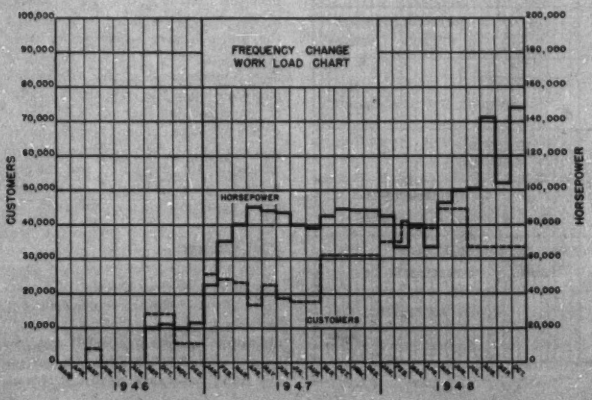
"The company and its contractors shall not be liable for loss or damage, including but without limitation, loss of use, production, or profits resulting from the adaptation of the customers' utilization equipment, or from the conversion of the company's system to a frequency of 60 cycles, except as provided in the paragraph immediately preceding."

Plan of Conversion

The plan of conversion as originally submitted to the Public Utilities Commission under the company's application contemplated that it would require approximately one year for organizing, scheduling, and pre-engineering the work, and $3^1/_2$ years for the physical conversion, with the project scheduled for completion by the middle of 1950.

Subsequent studies developed the fact that, because of the unprecedented load growth and increase in population in the territory served by the company, important savings could be effected by shortening the physical conversion to two years. Accordingly, the program was revised to proceed with the conversion in two areas simultaneously, with completion scheduled for October 26, 1948. For conversion purposes, the company's system was divided into nine geographical areas, with each served from a major 220-kv substation. Figure 5 shows the location of these areas and the reference number assigned to each. Under this plan, the Stone and Webster Engineering Corporation was assigned the survey, engineering, and conversion of commercial and industrial power customers in Areas 1 to 4 inclusive, together with two electric railway systems; and the Bechtel Corporation, Areas 5 to 9 inclusive; with each contractor assigned approximately 1,000,000 horsepower of connected load.

Figure 6. Chart of work loads by months



A distribution feeder was established as the basic unit for conversion. Studies were made by classification and number of customers, the horsepower of the connected load of each, and the number of deep-well turbine pumps connected to each feeder, and from these data, area cutover schedules were established so as to provide an optimum work load for the several contractors. Figure 6 shows the project work load chart in total number of customers and connected power load in horsepower to be cut over each month. It is to be noted that during May and June 1948, customer conversions reached a peak of 44,500 per month, and in June, 1948, the horsepower of connected load converted reaches a monthly peak of 116,000 horsepower. Figure 7 shows the "Tentative Schedule of Changes for 60-Cycle Operation," together with the total number of customers to be converted in each geographical area.

Detailed feeder cutover schedules were then prepared for each area and referred to the company's operating department for review and determination of the switching and load-carrying requirements, together with the required transmission and distribution line and station switching sequences. The unprecedented load growth experienced by the company since the cessation of hostilities has necessitated careful planning to co-ordinate the frequency change cutover schedules with the company's distribution and transmission system construction program. The many different operations of the several contractors involved in performing frequency change work has made adherence to the feeder cutover schedules imperative once established; changes in these schedules being made only in emergencies, or when it is found that the frequency change program can be expedited by revising the schedules. Present schedules call for the cutover of 1,151 feeders in 378 separate cuts or an average of three feeders per cut. The number of customers cutover range from one to a maximum of 6,123, with an average of 1,850 per feeder cut. The horsepower of connected load cutover at any one time ranges from approximately 500 to a maximum of 35,310, with an average of 5,430.

Basic Procedures

One of the most important phases of the program is the establishment and control of the flow of the enormous quantity of paper required to co-ordinate the activi-

ties of the various groups involved in effecting the conversion, so that, at the time of cutover of each customer, all of the work necessary for the proper conversion of the customers' equipment to the new frequency has been accomplished. Work flow charts were prepared and used as a base to control all operations of the project. These flow charts show the point of initiation of each item of paper, the action to be taken, the detailed forms to be prepared and processed, the lines of flow for each form, and the final disposition. Early studies indicated the only way in which the records could be practicably and economically handled was by the use of a punch-card and automatic sorting and tabulating machine system. As this was a unique application of tabulating machine operations, the records section had to develop new and unusual methods and procedures. It was determined that the conversion could best be handled by grouping customers into four general classifications:

Class 1—Agricultural power customers having deep-well direct-connected turbine pumps.

Class 2—Industrial customers with a connected load in excess of 50 horsepower.

Class 3—Commercial and small industrial customers with a connected load of less than 50 horsepower.

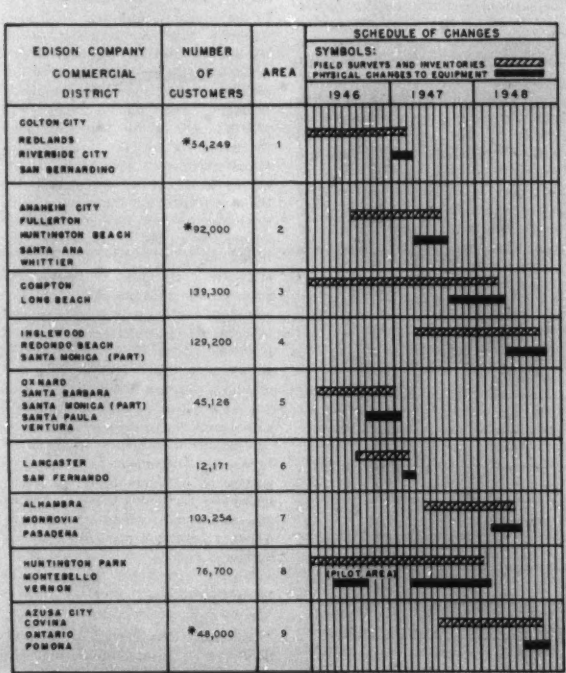
Class 4—All domestic customers.

Each customer is assigned a frequency change number with 17 digits which identify the customers as to area; the substation source of the feeder from which served; the feeder; the commercial district in which the customer is located; the meter read book; the customer's folio or account number; and the classification of his account. The following illustrates a typical frequency change number:

Area	Substation	Feeder	District
4	07	07	31

Meter Book	Folio	Classification
105	11,211	4

Figure 7. Tentative schedule of changes for 60-cycle operation



* INCLUDES WHOLESALE CITIES

The customer's frequency change number controls throughout the entire sequence of operations. For ready reference in handling customers' telephone calls in the area under conversion, an alphabetical list is prepared for each district, showing the customer's name, address, and frequency change number.

Operations are initiated by transcribing to a machine record card data on each customer from the company's district office records, together with the feeder from which the customer is served. From these data, a master record card is punched and the flow of the necessary paper is commenced. It is estimated that during peak operations, approximately 712,000 cards per month will be processed through the records section with a total of 11,200,000 cards for the project.

Customer records are established 12 months in advance of cutover. This allows four months for the processing of records and eight months for the survey and procurement of materials and the scheduling of the cutover with the customer. After these records have been set up in the records system of the frequency change operations, the local district offices, from which the customer records are obtained, are required to keep the frequency change department advised of any change in status of their customers, with the exception of domestic customers. Because of the relatively large number of changes continually occurring in the moving in and out of domestic customers on the company's system, no attempt is made to maintain the initial records of these customers prior to 60 days before cutover.

Approximately 60 days prior to the scheduled date of conversion, a recheck is made of the district office records of all customers connected to each feeder, and from this date on to the date of cutover, no changes are permitted to be made in the feeder system except under emergency conditions. This lead time of 60 days is maintained so that the mailing pieces outlining the company's program of conversion and the notices of cutover setting forth the date and hour of conversion can be directed to each customer in advance of the changeover and the contractors' forces scheduled to the customer's premises for the conversion.

Pilot Cutover

In order to try out the plan of conversion and to test the procedures developed for surveying and adapting customers' utilization equipment, a small area was isolated from the 50-cycle system and cutover to 60 cycles early in 1946. This in-

volved the cutover of about 4,400 customers with a connected load of 1,008 horsepower. The experience gained from the cutover of this pilot area proved invaluable in developing final conversion plans, procedures and tecaniques.

Connected Load and Number of Items of Equipment Requiring Conversion

Table III shows the connected load of the system together with an estimate of the number of items of equipment to be converted for 60-cycle operation. Included in these items are every type and kind of electrically driven or operated device, ranging from electric clocks to the largest steel mill motor drives. Of the 2,196,800 horsepower of connected power load requiring survey and engineering, 1,390,800 or 63.5 per cent will require conversion; the balance or 36.5 per cent being allowed to go "no change." Of the total number of items of equipment connected to the system, 53.4 per cent will go "no change", with the balance or 46.6 per cent requiring change.

Fundamental Engineering Principles

The application of engineering principles to the determination of the changes required to be made for the adaptation of the many thousands of different kinds, types, and makes of frequency-sensitive equipment and devices for operation at a higher frequency is not an exact science. It is a combination of fundamental theories, judgment, and experience. The engineers responsible for determining the adaptation required to reproduce at 60 cycles essentially the same performance which obtained when operating at 50 cycles have no control over the use, intended use or application of the equipment, or of its mechanical or electrical condition at the time of adaptation and, accordingly, must consider many factors in addition to the theoretical engineering principles involved, in determining the appropriate change. Accordingly, no attempt is made in this paper to discuss in detail the engineering theories involved but only to give a general outline of the basic engineering methods and procedures used in effecting the conversion of a few of the more general classes of equipment encountered in a frequency change program of this magnitude.

The effect of a frequency change on customers' equipment is manifest in various ways for different kinds of equipment, being visible in the case of rotating equipment but generally invisible in the case of nonrotating devices. The most significant and obvious effect on rotating equipment is an increase in speed directly proportional to the increase in frequency. Electrically, a change from 50 to 60 cycles is relatively small and, if it were not for the accompanying speed change, the amount of adaptation required for rotating machines would be materially less. Certain changes occur in the performance characteristics of electrical machines affecting magnetizing current, core loss, power factor, efficiency and torque. In nonrotating devices, the change is apparent only in a change of performance characteristics. It follows from the foregoing, that the principal objective in adapting frequency-sensitive utilization equipment for operation at a higher frequency is to compensate for the increase in speed in the simplest and most economical manner.

For conversion purposes, frequency-sensitive equipment may be divided into three general classes:

1. Equipment connected to the motor through flexible drives of various kinds and types which can be adapted by a simple mechanical change in the drive to maintain the original speed of the driven equipment. This represents about 54 per cent of the total items of equipment requiring adaptation.

2. Equipment directly connected to the motor, representing about 26 per cent of the total items requiring conversion and consisting principally of centrifugal pumps, deep-well turbine pumps, fans, blowers, and similar devices. Equipment in this class can in general be adapted by mechanical changes to the driven equipment, such as trimming or destaging pump impellers, and trimming or repitching fan blades and blower impellers.

3. Equipment requiring adaptation by an electrical change to the motor or driven unit, or a combination of electrical and mechanical changes. This represents approximately 20 per cent of the total items requiring adaptation.

Approximately one year was spent in engineering research to accumulate pertinent data on common and generally repetitive items of equipment and to establish basic adaptation principles, methods and procedures for the use of the several contractors. This was necessary in order to provide uniformity of procedure by the several contractors and to avoid duplication of engineering by these contractors. These data were tabulated and set forth in the frequency change manual, and this manual probably contains one of the most complete listing of domestic and commercial electrically-operated equipment extant.

Table III. 50-Cycle Customers, Connected Horsepower, and Items of Equipment Requiring Survey, Engineering, and Conversion

	Surveyed and Engineered	To Be Converted
Customers		
Agricultural	7,440	7,440
Industrial	30,426	30,426
Commercial	82,134	82,134
Domestic	580,000	580,000
Total	700,000	700,000
Horsepower		
50-cycle industrial and commercial power load	2,196,800	1,390,800
Per cent "No Change"		36.5
Items of Customers' Equipment		
Domestic washing machines	369,000	24,600
Domestic refrigerators	322,000	55,400
Electric phonographs	129,000	129,000
Electric clocks and timers	450,000	450,000
Fluorescent and mercury vapor light fixtures	330,000	198,000
Total items (all customers)	2,159,800	994,250
Per cent "No Change"		53.4
Deep-Well Turbine Pumps	7,800	7,250
Company will adapt		4,600
Customer will adapt		2,650
No change	550	
Meters		
Total number replaced	665,000	
Relagged and tested	374,500	
New meters installed	290,500	

Survey and Engineering Procedures

A field survey is made of each item of frequency-sensitive equipment on customers' premises, on forms provided for this purpose, in sufficient detail for the contractors' engineers to determine the appropriate change to be made. The initial survey of commercial and industrial customers' equipment is made approximately eight months in advance of the date of cutover except in the case of certain special items such as large synchronous motors, large air compressors, and heavy mill drives. These items were surveyed as much as two years in advance of the scheduled cutover date so as to provide sufficient time for the procurement of material and parts. The procurement of material and parts to meet the cutover schedules has been one of the most difficult problems.

From the survey data, the contractors' engineers analyze the make, type, and application of each item of equipment and determine the most appropriate and economical change consistent with recognized engineering and machine design standards. Frequently, electrical or mechanical tests are necessary to obtain information for the engineers' use in determining the proper adaptation, to record the 50-cycle performance of the equipment, and to demonstrate that the 60-cycle

operation essentially duplicates the 50-cycle operation. Such tests include load and performance tests on motors and generators; hydraulic tests of head, capacity, and efficiency on deep well turbine pumps and large centrifugal pumps; and mechanical tests of vibration, air delivery and noise levels. Machines are often found in use in a service for which they were not originally intended by the designer or manufacturer and, in other cases, customers have taken advantage of the increased torque available from a 60-cycle induction motor operating on 50 cycles.

In the large majority of cases, the appropriate change is determined without need of consultation with the manufacturer but, in the case of certain equipment, such as large synchronous motors, motor-generator sets, rotary converters, blowers, pumps, and mill drives, it is necessary to obtain data not obtainable by physical survey in order to compute 60-cycle performance characteristics and mechanical stresses. The manufacturers have co-operated generously in supplying information, but in many instances, manufacturers have had no occasion to determine how their appliances or equipment would perform when operated at other than the design speed or frequency and, accordingly, have been unable to furnish all of the desired information without further engineering development on their part.

As shown in Table III, 53 per cent of the total number of items of customers' equipment in operation on the company's system will operate satisfactorily at the higher frequency without change, provided the equipment is in good mechanical condition. Included in the 53 per cent are small air and refrigerating compressor units commonly found in garages, service stations, butcher shops, and other commercial establishments. Also included are a number of machine tools installed during the war which were standard 60-cycle units and were not modified by the user for operation at 50 cycles.

Electrical Changes

Transformers in general benefit by an increase in frequency. Table IV shows the effect of such a change on distribution and power transformers.

The efficiency of the transformer is usually improved up to approximately normal full-load because the decrease in iron loss exceeds the increase in copper loss. At over-load operation, the efficiency may be slightly less if the increase in copper loss at the higher loads becomes greater than the decrease in iron loss. Constant-voltage type transformers with tuned circuits or saturation characteristics to control secondary output voltage require modification since the output voltage would otherwise be stabilized at a value considerably higher than the rated voltage. In the smaller sizes, it is generally more economical to replace the transformer. In sizes of 1 kva and larger, modification is usually accomplished by removing the proper amount of core iron and changing the value of the capacitance in the tuned circuit.

Induction and synchronous motors are affected by a change in frequency in the same way as transformers. In addition, the synchronous speed is increased 20 per cent and starting and pull-out torques are reduced. Full load efficiency will, in general, remain the same or will increase slightly. Bearing friction and windage losses increase but these constitute such a small percentage of the total losses that they do not importantly affect the efficiency. Power factor will improve because the magnetizing component of the exciting current decreases considerably. In the case of induction motors, the running torque over the normal range of operation is dependent upon the connected load, and the motor will inherently supply the torque required at the expense of increase slip or a reduction in speed. With the same horsepower output after adaptation, the per cent slip at the 60-cycle speed will remain essentially the same as at 50 cycles. For the average general purpose induction motor, the slip is approximately 3 per cent. If the full load running torque required at 60 cycles is the same as obtained at 50 cycles, the per cent slip will increase to approximately 120 per cent of the 50-cycle value.

The maximum torque varies inversely to some power less than the square of the frequency. Computations and tests indicate that at 60 cycles it will be approximately 73 per cent of the 50-cycle torque. Values less than this obtain on very large motors but will generally approach 69 per cent as a limit, whereas values above 73 per cent will in general obtain on the smaller integral and fractional horsepower motors. Although the maximum torque available decreases, in general application this is of no importance since the very high loads required to reach the point of pull-out practically never occur.

Table IV. Per Cent Change in Performance of 50-Cycle Transformers When Operated at 60 Cycles

	Small Transformers $1^1/_2$ to 25 Kva	Medium Transformers $37^1/_2$ to 200 Kva
Iron losses	80–90	80–90
Exciting current	40–60	40–60
Total copper loss	101	103
Impedance	109	115
Regulation 100 per cent power factor	100	108
Regulation 80 per cent power factor	107	111

Starting torques decrease approximately as the square of the frequency and by tests, the 60-cycle value was found to vary between 59 and 65 per cent of the 50-cycle value; the smaller the motor the closer the value approaches 65 per cent. However, as these torques increase as the square of the voltage, they are not reduced below the design torques by the change in frequency to the extent shown above because the company's nominal service voltage is 10 per cent above the standard design voltages of general purpose motors.

Motors must be modified when it is essential to maintain approximately the same speed as obtained at 50-cycle operation or when it is necessary to maintain the 50-cycle starting and pull-out torques. Speed is maintained by reconnecting, rewinding, or repoling to an increased number of poles. Torque is maintained by rewinding with fewer turns and larger wire. In the case of wound rotor or synchronous motors a rewind or reconnection of the stator winding also requires a change in the number of rotor poles.

In the case of synchronous motors driving reciprocating loads such as air or ammonia compressors, consideration must be given to the effect on motor current pulsation. Generally, no significant change in pulsation occurs but occasionally, it is found necessary to change the WR^2 of the fly-wheel. Each case must be individually studied.

The rewinding of a-c machines for an increased number of poles often results in windings with unequal numbers of coils per pole group. Almost all of the practical winding combinations were investigated and catalogued in order that only those resulting in satisfactory motor performance would be used. The major manufacturers have been very co-operative in checking any unusual proposed windings. By the use of such windings, it has been possible to reconnect instead of rewind in many cases, thereby effecting important economies in the cost of motor conversions.

Most motor-generator sets are allowed to increase in speed, changes being made to the d-c generators to obtain the proper output characteristics. The parts of a motor-generator or rotary converter

chiefly affected by increased centrifugal forces are armature bands, wedges, commutators, damper windings, field pole pieces, and pole bobbins. These must be checked carefully to be sure that safe stress limits will not be exceeded. To obtain the data necessary to make the proper modifications and adjustments at the time of speed up, preconversion tests are made, the kind and extent of these tests depending on the size and importance of the unit. In general, additional field resistance is all that is required for the adaptation of generators where regulation or stability is not critical. Machines used in parallel operation or those requiring precise voltage regulation may require a reduction in the cross-section of the magnetic circuit in order to restore saturation and maintain stability. This is usually accomplished by slotting the sides of the main pole pieces, the size of the slot being predetermined by calculations based on test data. Some unique techniques have been developed for carrying out this operation, such as flame-cutting the slots in pole cores integral with the frame of the generator.

Arc welder motor-generator sets constitute a special class. Multiple operator units use constant potential generators and ballast resistors and are adapted in accordance with standard procedures for general purpose motor-generator sets. Single operator units have differential compound characteristics and are readily adapted for 60-cycle operation by the installation of suitable resistors in the various control fields. Values of resistance to be inserted are selected so as to give the same or a slightly higher open circuit voltage as obtained on 50 cycles.

Industrial control devices depend on electromagnetic forces to activate contactors, relays, and valves. The National Electrical Manufacturers Association rates magnetic contactors in ten sizes from 00 to 8. Sizes 00 to 2 and some of size 3 have resistance-reactance ratios such that the loss of pull due to the increased frequency is not sufficient to require correction. The larger sizes are adapted either by the procurement of new coils or by removing 15 per cent of the turns. In critical cases, a tap may be brought out of the coil for 50-cycle operation prior to conversion.

Electric discharge types of lighting such as fluorescent, neon, and mercury vapor lights, of which it is estimated that there are approximately 330,000 of the fluorescent and mercury vapor types on the company's system, are equipped with current limiting ballasts. These ballasts contain a reactor or a combination of condenser and reactor which are affected by a change in frequency. Those using single reactor ballasts such as neon lights and small single lamp fluorescents require no modification. In the larger sizes, however, such as the widely used 2-lamp 40-watt high power factor type, a change to 60 cycles causes a 10 to 20 per cent reduction in current in the lag lamp with a resulting decrease in light output, and approximately a 70 per cent increase in the lead lamp current with a considerable increase in the light output. This causes injurious heating of the ballasts and importantly reduces the lamp-life. New 60-cycle ballasts were unobtainable in the quantities required to meet the conversion schedule, and, as the cost of replacement was high, an adaptor was developed by the department's engineers for use with the existing 50-cycle ballasts to restore the lamp currents to normal 50-cycle values when used on 60 cycles. This adaptor consists of a 3-winding transformer reactor and approximately 50 per cent of the 40-watt 2-lamp fixtures are converted by their use; the remaining 50 per cent being converted by installing 60-cycle ballasts. It is of particular interest that the survey of fixtures to date shows that approximately 40 per cent are equipped with 60-cycle ballasts which have been operating successfully at 50 cycles. Large installations of high intensity mercury vapor lighting were encountered in aircraft plants constructed during the war. Most of these are 2-lamp 400-watt fixtures and are adapted by use of an autotransformer to reduce the voltage across the capacitor together with a tap change on the primary of the ballast transformer.

Mechanical Changes

The largest class of mechanically adaptable equipment other than flexible driven equipment consists of centrifugal and turbine type pumps. It is estimated that a total of 45,000 pumps of various makes and types will be encountered and adapted during the program. This includes refinery process pumps handling acids, propane, crude oil, caustics, molten salt, light fraction petroleum products, water, and sewage.

Theoretically, a 20 per cent increase in the speed of centrifugal and turbine pumps results in a 20 per cent increase in capacity, a 44 per cent increase in developed head, and a 73 per cent increase in brake horsepower. As stated previously, the majority of pumps can be adapted by trimming impellers, if above minimum diameters, or in the case of multiple stage deep-well turbine pumps, by trimming, destaging or both. Theoretically calculated trims could not be used and it was therefore necessary to develop performance graphs of the various types and makes of pumps based upon empirical values and experience. However, caution must be used in applying these graphs as it is often found upon removal of the impeller that wear has taken place and the amount of trim must be modified accordingly.

In four of the major oil refineries, a total of 2,300 pumps have been engineered for change in the foregoing manner. Of this total, only 100 pumps are being replaced because of the impossibility of adapting them to reproduce the same performance on 60 cycles as obtained on 50 cycles.

The problems involved in adapting deep-well turbine pumps, of which it is estimated there are a total of 7,800 on the company's system, are many and complex, being aggravated by changed pumping conditions which have taken place since their initial installation, and in many cases, by receding water tables. The most important factor in engineering the required changes is obtaining accurate test data on the pump's characteristics at 50 cycles. Performance curves when available are obtained also from the manufacturer for each type and size of pump. With these data, it has been possible to determine with reasonable accuracy the change required to reproduce the same head, capacity and efficiency at 60 cycles as obtained at 50 cycles. In many instances, it has been possible to increase the efficiency of the pump. The poor mechanical condition of many deep-well turbine pumps has added also to the problem of pulling, adapting, and reinstalling them for the higher frequency.

Fans and blowers constitute the second largest group of mechanically adaptable equipment. In general, the same engineering principles are employed in adapting them as those used in adapting pumps. High-speed fan or blower impellers frequently require dynamic balancing to insure satisfactory operation at the higher speed after trimming.

Large compressors present a major problem as these units are usually driven by synchronous motors where WR^2 must be taken into consideration. Where it is necessary to maintain the existing compressor capacity, and when the compressor can otherwise be speeded up, they can often be mechanically adapted by either installing shorter disk cranks changing head clearances, installing cylinder liners and smaller pistons or a combination of two or more of the previously mentioned

methods. Where increased compressor capacity is permissible, some large compressors can be adapted simply by the installation of modern valves to replace old-style valves.

Many types and kinds of unusual equipment are encountered such as expellors, attrition mills, hammer mills, electric organs, and so forth, all requiring special consideration in determining the appropriate adaptation. For example, approximately 200 hammer mills, totalling 10,000 horsepower or an average of 50 horsepower per unit, have been encountered. These are usually driven by 2-pole motors designed for 60-cycle operation but the use of these mills varies widely and, where one mill can go "no change," a similar mill used for a different purpose must be adapted to maintain speed.

Many unusual electrical and mechanical problems have been encountered in the project requiring considerable ingenuity on the part of the engineers, however, space does not permit of detailing these problems and their solutions.

Survey and Conversion Procedures

The assignment of customers' equipment to the contractors for survey and conversion is made by classification of the account, namely: domestic, commercial, industrial and agricultural having direct-connected deep-well turbine pumps. Electric railways are handled separately as a special assignment.

The procedure followed in the survey and conversion of each class of customer differs with each contractor, and for this reason will be set forth separately hereinafter.

Each contractor operates as an independent contractor but under the direction of the frequency change department for co-ordination of the work and to assure uniformity of policy. Items of equipment which are common to all customers, such as timing devices and fluorescent lighting, are reassigned to the contractor performing that phase of the conversion. As an example, when the survey men of the domestic contractor find timers, home workshop equipment, small domestic pumps, incubators, and other equipment of a specialty nature on the domestic customers' premises, this equipment is reassigned to the appropriate contractor for engineering and conversion. The domestic contractor is employed to perform the conversion of household appliances only, and is not equipped with the materials or the craftsmen necessary to perform specialty work. This plan of reassignment of specialty items of equipment has been highly satisfactory and has eliminated duplicating stocks of materials and craftsmen having special skills, in each of the contractor's organizations.

The company maintains 26 district offices throughout the territory being cutover to 60 cycles for the conduct of its regular business, and as the frequency change operations progress throughout the areas of conversion, the field organization of the frequency change department and its contractors establish field headquarters in each district. This is done so that a close relationship can be maintained between the district office and the frequency change personnel in all matters concerning customer relations, and the handling of telephone inquiries from the customers. Clock depots are located also in each district for the convenience of customers in cycle changing or exchanging portable clocks. As a result of the continually moving operations, it is necessary to provide office, clock-exchange, and headquarters building space for the frequency change operations of the department and its contractors and, when working more than 25 miles from headquarters, to provide transportation and hotel accommodations for the men.

Domestic Customer Conversion

Frequency-sensitive appliances of approximately 580,000 domestic customers will be cut over during the project. This represents 83 per cent of the total customers involved.

The domestic contractor receives his survey assignments on prepunched and printed tabulating cards showing the name, address and frequency change number of each customer. On the reverse side of the card, there is printed a list of appliances generally found in the home. These cards are assigned to survey crews who list the items of appliances found on the customer's premises, such as washing machines, refrigerators, record players, and so forth, and as they are surveyed, a printed tag is placed on each item of equipment designating whether or not a change is required. At this time, the customer is also advised of the details of the program, the date of cutover, and the approximate time the conversion crews will call at the customer's residence to adapt the frequency-sensitive appliances designed. The survey is made approximately three weeks in advance of the date of cutover and because of the relatively short time to cutover, the survey man (in the course of conversation with the customer) changes the bushings on record players having this type of drive. From the master inventory card prepared by the survey man, a separate conversion card is made out for each appliance requiring change showing the make and model and the materials required for the change. These cards are then assigned to the conversion crews for the adaptation of the appliances, a few days in advance of the date of cutover.

The domestic contractor conducts his field operations from mobile trailer units which are moved into each district as the conversion progresses. Each field unit is self-contained with office trailers for the supervisory personnel, warehouse trailers, and portable shops.

All appliances are adapted in the home with the exception of gear driven washing machines and gear driven record players which are transported to the portable shops for conversion.

The domestic contractor employs approximately 184 men, of which 66 are on survey and 83 on conversion, the remainder being supervisory and clerical personnel. Experience to date in the conversion of domestic customers shows that 40 per cent of all customers in this class have frequency-sensitive devices requiring adaptation. The survey of domestic customers is currently being made at the average rate of 28 customers per survey man day, and conversion at the rate of 8 customers per man day.

Commercial Customer Conversion

The 82,134 commercial customers having a connected load of less than 50 horsepower comprise approximately 11.7 per cent of the total number of customers to be cut over, with a connected load of approximately 114,000 horsepower.

Included in the commercial classification are restaurants, markets, service stations, small machine shops, doctors, dentists, and other types of small business establishments.

A large majority of the equipment encountered in this class is repetitive and the methods of conversion are set forth in the data book section of the frequency change manual. However, certain equipment, such as walk-in type refrigerating units, medium size compressors, and certain machine shop tools require separate engineering and procurement of materials. Accordingly, all commercial customers are surveyed approximately three months in advance of the date of cutover.

Since a large percentage of the equipment encountered in this group is driven by single phase motors, special single

phase crews have been organized to make starter spring changes when required. While theoretically, a large proportion of the single phase commercial refrigerating units could go "no change," experience has shown that this equipment is operated with little maintenance and, on speed up, would not transfer from the starting winding to the running winding. As a result, in order to avoid equipment failures and food spoilage, the policy has been adopted of maintaining 50-cycle speed by changing the driven pulley on practically all commercial refrigeration units and changing the setting of the transfer switch on the starting winding of the motors.

Also found in this group are special timing devices, such as time stamps, bank check recorders, X-ray timers, diathermy timers, hospital timers, police and fire station timers, photographic timing equipment, and so forth, much of which must be converted simultaneously with the feeder cutover. These items are handled by special crews of the clock contractor and are given priority of conversion on the day of cutover. All nonportable clocks are adapted on the customer's premises but the portable clocks are required to be brought into the clock depots for exchange or cycle-change, as hereinafter discussed.

Industrial Customer Conversion

The industrial group having a connected load in excess of 50 horsepower comprises approximately 30,426 customers or 4.3 per cent of the total, with a connected load of 1,619,288 horsepower or 82 per cent of the total connected load to be cut over. The engineering determinations and the procurement of materials require that the survey be made at least eight months in advance of the date of cutover.

Customers in this class are personally contacted by one of the department's commercial division employees in company with a representative of the contractor. At this initial contact, the frequency change program is explained arrangements made for the contractor to survey the equipment. Since in many industrial plants the shutting down of a piece of equipment for survey may interrupt the customer's manufacturing or processing operations, every effort is made to plan and schedule the survey so as to provide the minimum interference with the customer's operations.

Data obtained by the contractor's survey men are processed through the contractor's engineering division and the proper method of adaptation determined. Items of equipment involving costly or unusual changes are referred to the engineers of the frequency change department for review or assistance in determining the propriety of the changes. When the final determination has been made on all items of equipment in a customer's plant, the customer is then advised of the proposed changes and arrangements made for the out-of-service time required for adaptation.

The preponderance of equipment in the average industrial plant can be preconverted with only a few of the more critical items requiring simultaneous change with the cutover of the feeder. Where it is found impossible to take out of service a critical item of equipment, a temporary frequency changer is installed and the conversion postponed for a more favorable time. While most of the changes in industrial customers' equipment can be performed by reasonably competent electrical or mechanical craftsmen, the contractors found it necessary to train their foremen and field supervisors in the handling of emergencies and unusual conditions which inevitably arise in a program of this size. The field operations of the contractors are carried out from a main field headquarters located in each area and subfield offices located in trailers, each of the latter generally consisting of a warehouse trailer, a small repair shop, and a portable construction house for the clerical and supervisory personnel. Any complaints arising after cutover are handled by trouble crews especially trained for this purpose.

Agricultural Customer Conversion

Agricultural customers comprise approximately 7,400 customers or 1 per cent of the total, with a connected load of 252,537 horsepower or 12.3 per cent of the total power load to be converted.

Approximately six months in advance of the cutover, a representative of the company calls on each agricultural customer to outline the conversion program and the method of adaptation employed by the company in adapting deep-well turbine pumps. At this time the customer is offered the option of an allowance in lieu of conversion should he prefer to have his own well contractor perform the work or, if he elects, the company will adapt the pump without cost with their own crews. As is shown in Table III, approximately 34 per cent of the customers elect to accept the allowance in the amount of the company's cost so that they can perform repairs or modernize the pump when removed from the well for adaptation. If the customer elects to have the company make the adaptation, the customer is advised that a hydraulic and electric test will be made on the pump so as to establish the 50-cycle pump characteristics necessary for determining the adaptation for the 60-cycle speed. After the tests have been completed and the method of conversion determined, the customer is again contacted, given a summary of the tests, and advised of the proposed method of conversion.

Since the output of a deep-well pump is in part dependent on the draw-down of the water during the pumping cycle and in part on the operating head, which may often be varied by the operation of valves, every effort is made to obtain accurate data at the time of tests as to the standing water level, draw-down during operation, horsepower consumption, output at various heads, and any other data which will assist in making the proper engineering determinations.

The pulling, adaptation, and reinstalling of deep-well turbine pumps is handled by crews trained by the department. These crews are equipped with 20 light-duty and 5 heavy-duty mobile pump-pulling rigs especially constructed in accordance with specifications drawn up by the department's engineering division. The crews operate out of field headquarters, at which mobile machine shops are located, together with the necessary supervisory and clerical staffs. Pump bowls are brought to the portable machine shops for trimming and/or destaging, and any defective parts are called to the customer's attention in order that he may have them repaired or replaced if he so desires. A large majority of the customers take advantage of this opportunity to put their pumps in good operating condition.

Occasionally pumps are found to be stuck in the well and, after making a reasonable attempt to pull them, the company then offers to either install an offset V-belt drive or to give the customer an allowance in lieu of conversion equivalent to the cost of such an installation. Offset drive heads have been designed by the engineering division and are manufactured locally according to the company's specifications. At the peak of operations, the pump conversion section of the department had a total of 117 employees engaged in adapting deep-well turbine pumps.

Electric Railways

The two railway systems served by the company have connected loads of approximately 151,000 horsepower, mainly in

Figure 8. Mobile mercury arc rectifier substation

Showing from left to right: Harmonic filter, rectifier house, heat exchanger, main transformer, transformer cable pothead rack, auxiliary control switches, auto transformers, and oil circuit breakers

motor-generator sets and rotary converters ranging in size from 400 to 1,500 kw. Each of these companies owns and operates their own distribution system which are fed from a number of points of delivery from the company's system. The two systems include 63 substations with 77 motor-generator sets, 32 rotary converters, and 6 mercury arc rectifiers. Of the 77 motor-generator sets, 25 are driven by synchronous motors.

Conversion of the railway units offered a number of problems, not the least of which was the necessity of constructing two mobile d-c substations to replace temporarily the units as they are removed from service for adaptation.

One of the mobile stations consists of a 1,400 horsepower 2,200-volt synchronous motor direct-connected to a 1,000-kw 600-volt d-c generator with associated controls, all mounted on a rubber-tired trailer for outdoor use. A second trailer carries a bank of three 333 kva 16.5/2.3 kv transformers with oil switch and disconnecting switches.

The second mobile unit consists of a General Electric 6-phase 12-tank mercury arc rectifier rated at 2,400 kw with a control cubicle for 50 and 60 cycles, anode and cathode circuit breakers, and their required controls, all mounted in a weatherproof housing on a rubber-tired tailer. A second trailer carried a 4,500-kva 3-phase to 6-phase 11,500/611-volt transformer and auxiliaries, and a heat exchanger for the rectifier.

The rectifier and transformer were obtained from a War Assets plant where one transformer supplied two rectifiers, hence the reason for the oversized transformer. Because the voltage of this transformer does not conform to the voltage of the railway distribution system, a bank of 3-phase autotransformers rated 16.5/11.5 kv is mounted on a third trailer. This trailer carries also the necessary oil circuit breakers and disconnects. This mobile rectifier substation is somewhat unique and is believed to be the only one of its kind. Used also in connection with the railway conversion are two sets of load boxes mounted on trailers with variable taps to allow a wide range of current values. Figure 8 is a picture of the portable rectifier substation equipment lined up outside of one of the railway substations.

As the railway distribution systems are served at 17 points of delivery, it was necessary to plan the conversion of the two systems concurrent with the conversion of the company's system at the various feed points. The co-ordination of the conversion of the two railway systems with that of the company's main system, and the transporting, connecting, and disconnecting of the portable substations has required careful planning and scheduling by both the railways and the company.

Conversion of Electric Clocks and Timing Devices

It is estimated that approximately 350,000 electric clocks and timing devices will require adaptation for operation at 60 cycles. Since the advent of electric clocks, a number of the early or small manufacturers discontinued the manufacture of clocks which made it impossible to obtain replacement parts and, therefore, the company established an exchange plan whereby the customer has the option of having his clock cycle-changed without cost or to exchange his clock for a new and modern one with a small cash payment. The clocks being used for exchange purposes are Telechron Incorporated and General Electric Company.

Customers are required to bring portable clocks for cycle-change or exchange to conveniently located clock depots in the area under conversion.

A majority of the timers used for gas and electric ranges are built into the range and are adapted on the customer's premises by the clock contractor's field crews. If the range timer is obsolete and parts are not obtainable, the customer is offered a replacement timer.

The use of electric timing devices for the precision control of processes has grown tremendously in the last ten years, and many special applications have been encountered. In most cases, the frequency-sensitive component of these timing devices is a standard clock type synchronous motor with a rating of one or two watts and its adaptation for 60-cycle operation consists in general of changing the clock rotor. Arrangements are made with the respective manufacturers to adapt the special and critical timing devices. The handling of the conversion of clocks and timers is a sizable project itself and, at the peak of the program 155 men were employed by the department and the clock contractor for this phase of the work.

Conversion of Meters

As all watthour meters are frequency-sensitive, it is necessary to either replace or adapt approximately 665,000 50-cycle meters on the company's system as well as a number of customer-owned meters. It had been originally planned to retire 275,000 obsolete-type meters during the program. However, difficulty in obtaining delivery of the required number of new meters made it necessary to substantially modify this program, thereby adding importantly to the number of meters to be relagged. A special meter division of the frequency change department was established to handle the replacement or removal, relagging, and reinstallation of all frequency-sensitive meters. Trained meter men in the number required were not available and it was necessary to carry on an intensive training program in order to obtain the required complement of men. Meter conversion is divided into two major operations, namely, field operations or the replacement of the meters on the customer's premises, and relagging and repair operations which are performed at a central shop on a two-shift basis. Special meter test tables were designed and constructed for this work.

Information concerning the customer's meter is secured at the same time that the customer's record is picked up in the commercial district office, approximately one year in advance of cutover. This information includes the type, code, year set, mounting, customer's frequency change number, and the name and address of the customer. The information thus obtained is punched in a tabulating card which is used as an order by the meter division for replacing the 50-cycle meter. The meter setter fills in on the card the meter number, code and reading of the 50-cycle meter before it is removed, and similar information for the 60-cycle meter installed, and the card is then returned to the records section where tabulated lists are prepared and forwarded to the stores and accounting departments and the district commercial offices of the company.

Integrating meters are changed approximately 45 days in advance of cutover, except in the case of large power meters which, together with demand meters, are replaced simultaneously with cutover. Field operations are carried out from temporary field offices.

At the peak of operations, 152 men were employed in the meter division.

Transportation

The work of converting customers' equipment extends over a service area of approximately 18,000 square miles and the problem of transporting equipment, materials, and employees, and of housing the employees in the more remote areas is one of considerable magnitude. When physical operations were commenced in 1946, automobiles and trucks were difficult to procure. At the peak of operations, there were approximately 344 passenger cars, 388 trucks, 12 office-type trailers, 58 miscellaneous trailers, and 153 personally-owned cars used under operating agreements, or a total of approximately 953 motor vehicles in use by the department and its contractors. This equipment has traveled as much as 771,000 miles in one month.

Equipment and Material Pools

In order to facilitate the adaptation of customers' equipment and at the same time provide equipment for emergency service during conversion, it was found necessary and economic to establish equipment and material pools consisting of special mobile or portable equipment motors, and pulleys for the use of all contractors under the administration of the frequency change department. The equipment pool contains the equipment shown in Table V.

In addition to the foregoing, the pool contains distribution transformers of single and 3-phase types ranging in size from 100 to 1,200 kva each. The frequency changers are designed to provide frequencies of 50 and 60 cycles and voltages of 120 to 480 volts. The principal use of the equipment pool is for the preconversion and postconversion of critical items of equipment in industrial plants, and to provide emergency service at 50 or 60 cycles as required. The two 1,000-kva frequency changers are used primarily for supplying 50 or 60-cycle energy to the two railway systems during the testing and conversion of their substation equipment and to motion picture studios during the conversion of their motor-generator sets.

The motor pool consists of 800 motors of various sizes, speeds and voltages, ranging in size from fractional and integral horsepower single-phase motors to integral horsepower 3-phase induction and synchronous motors from 1 to 800 horsepower. These motors are used as replacement motors in the event of emergency or for temporary replacements during the time the customer's motor is out for rewind or reconnecting.

The pulley pool consists of approximately 20,000 items of various types, sizes, widths, and diameters of pulleys made up of cast iron, split steel, and pressed paper; together with single and multigroove sheaves of various kinds and types. All pulleys and sheaves removed from the customer's equipment are processed through a small machine shop set up in conjunction with the pool stock, and placed in first-class condition for reuse.

Customer Relations

The maintenance of customer relations in a program of this kind is highly important and involves the co-operation and complete understanding of the objective by all parties concerned. Where heretofore the company's operations normally stopped at the meter, the operations of cycle changing customers' equipment commence at the meter. This involves many problems never before encountered by the company and required the development of new policies and procedures entirely unrelated to the normal operations of the company.

Recognizing that many situations would arise in adapting customers' equipment which not only involved the technical aspects of converting hundreds of thousands of items of equipment but also that of entering the homes, offices, business establishments and plants of over 700,000 customers to perform the work, the frequency change department organized to handle all customer relation problems with its own personnel.

Approximately 30 days in advance of the cutover of feeders in a district, the department opens a frequency change office, staffed with personnel to receive customer calls and to handle customer relation problems arising in connection with the work. This office remains open 60 days after cutover of the last feeder in the district, for by that time the customer relation problems have diminished to the point where the regular district personnel can handle the few remaining problems, with occasional assistance from the frequency change department.

While the problems encountered in changing the frequency of a system of this magnitude are many and the costs large, the project is being completed on schedule and, it is believed, to the satisfaction of the many customers of the company as evidenced by the many expressions of appreciation received.

Table V

Quantity	Equipment	Size
53	Frequency changers, kilovolt-amperes	11/2 to 1,000
2	Gasoline-driven single phase alternators, kilowatts	10 kw
4	Diesel-driven 3-phase alternators, kilowatts	15 to 20
6	Direct current motor-generator sets, kilowatts	1 to 1,000
1	Direct current rectifier, kilowatts	2,400
2	Portable substations, kilovolt-amperes	3,000
5	Diesel-driven air compressors, cubic feet per minute	315
5	Water tank trucks, gallons	300 to 1,50

Discussion

T. M. Morong (Southern California Edison Company, Los Angeles, Calif.): The subject paper, because of the extensive nature of the project, could not go into any appreciable detail concerning the various phases of its major activities. Therefore, it is intended to supplement that portion as related to the electrical engineering section's activities in converting customers' equipment. When you consider that the equipment of the customers connected to our lines includes practically every type of electrical apparatus ever designed and in addition is applied to almost every known type of utilization apparatus and process, it be-

Table I. D-C Machine Conversion Estimated for Complete Project

No change and adjust after cutover	676
Resistor addition	786
Rheostat blocking	60
Pole notching	243
Armature rebuilding	64
Motor rewind or reconnect	64
Belt drive installation	3
Pulley change	205
Rectifier installation	5
Purchase new generator	2
Change shims on main poles	16
New rotating element	5
Total	2,129

comes apparent that here again this phase of our activity would require several volumes of books to cover the common types of apparatus encountered.

In order to make economical and satisfactory customer conversions, it was necessary to investigate means of simply modifying the apparatus to give essentially the same operation as previously enjoyed on 50 cycles. Once the actual conversion of customers' equipment was started in the fall of 1945, another general phase of activity commenced, that of advising our customers how to purchase and, where necessary, to modify new 60-cycle equipment for temporary operation on 50 cycles, the reverse of our usual procedure.

Further activities of the electrical engineering section includes development on existing designs, performance testing, checking mechanical stresses of rotating machinery, cost comparisons of alternative methods of conversion, and extensive studies of customers' unusual, and in some cases, highly confidential processes of their utilization equipment. The apparatus involved covered those such as appliances used by domestic customers, commercial customers' equipment as found in hospitals, stores, restaurants and office buildings. Industrial installations such as steel mills, cement plants, refineries, rubber mills, glass factories, automobile assembly plants, in fact about every known type of manufacturing plant, agricultural equipment consisting mainly of deep-well pumps, electric railway substations' large rotating apparatus, and communication apparatus in telephone, radio, and television stations.

It is readily discernible from such broad activities that it is impossible to impart to you any detailed information. There follows, however, a few salient points of a general nature which will give you a brief over-all resume of the conversion of motors and d-c motor generator sets.

As pointed out previously in the subject paper, a large number of motors are able to operate on 60 cycles without change to the motor because of the nature of the application permitting, and a considerable number were already an integral part of 60-cycle apparatus being operated by customers on 50 cycles. In fact, the general approach to all appliances such as domestic refrigerators, washing machines, and so forth, was to allow 90 per cent of those to go no change and the other 10 per cent to have a mechanical drive change.

As a result of this procedure, an estimated total number of 900 motors will be rewound because of burnout, based on records to date, of a most insignificant figure of $^1/_{10}$ of one per cent. The causes of burnouts in most cases have been due to poor maintenance of the equipment requiring an excess of torque over that which the motor is capable of producing and in some cases because of a change made initially in the starter spring for 50-cycle operation.

A total of more than a half million of induction motors is estimated to be connected by commercial and industrial customers to the company lines of which it is estimated a total of 3,700, or less than one per cent, will require rewinding or reconnecting for torque or repoling for speed change. Only 250 new motors will have been installed by the end of the project.

A total of 475 synchronous motors have been surveyed, of which 308, or 65 per cent, were allowed to speed-up, 59 or 12 per cent repoled, 77 or 16 per cent with a new rotor furnished, and 15 new motors or 3 per cent of the total.

It is quite significant from the foregoing figures that very little 50-cycle equipment cannot be modified to operate satisfactorily on 60 cycles. In fact the few cases where new motors were supplied, in many instances the customer desired increased horsepower and paid for the additional plant improvement, and in other cases, it was more economical to purchase new motors because of obsolescence of dies, and so forth, to make replacement parts.

In conclusion, a breakdown of the conversion of d-c motor-generator sets other than welding sets is given in Table I of the discussion to indicate the various methods by which this is being accomplished.

Electrical Developments in the Pulp and Paper Industry

R. R. BAKER
ASSOCIATE AIEE

THE EFFICIENT GENERATION and distribution of electric energy, as well as its effective utilization, is essential in the operation of the pulp and paper mills of the United States. Careful consideration must be given to developments in electric equipment which permit of the most efficient overall plant operations. In the year 1947 it is estimated that the pulp and paper industry used some 14,000,000,000 kilowatt-hours in producing approximately 21,000,000 tons of paper. In the year 1945, the last year for which complete figures are available, among other major industries in the United States, the pulp and paper industry ranked first, in power per ton of product (700 kilowatt-hours), third, in installed generating capacity (1,348,000 kw), fourth, in total kilowatt-hours (11,450,000,000 kilowatt-hours).

The actual power requirements vary for the different grades of pulp and paper as well as for the different operating conditions encountered in mills producing similar grades. From operating results in various mills, it is estimated that the average mill load in horsepower per ton daily production is approximately as shown in Table I.

The per capita consumption of paper products is higher in the United States than in any other country in the world. Statistics indicate a daily consumption of approximately 0.85 pound per individual. This high daily consumption is undoubtedly due to the wide diversity of paper uses. Paper which was originally used for printing almost exclusively, now has a multitude of other uses, such as building papers, wrapping and bag papers, towelling and tissue, boxboard and container boards, drinking cups and food containers, as well as a host of others. To meet the demands of this ever expanding market it has been necessary for the industry to convert from a hand craft to a highly mechanized industry. Each craftsman in the modern plant requires an investment of more than $18,000 and utilizes some 15 horsepower in power-driven equipment in producing approximately 800 pounds of finished paper per day.

Due to the many inherent advantages which electric energy affords in power generation, distribution and utilization, it has been almost universally adopted by the pulp and paper industry as its source of motive power. It is estimated that the industry is some 88 per cent electrified. This form of energy may be developed at the point of greatest economy. It may be transmitted long distances with only slight losses, to a mill site which may be located most suitably for converting the raw materials into the finished product. Electric power distribution presents no serious problems and the mills may be laid out without other consideration than that of the most economical conversion of the raw materials. The many different operating characteristics available in the modern a-c and d-c motors together with the features of either manual or automatic control permit economic operation of all power driven equipment throughout the mill.

Table I

Pulp	Horsepower
Chemical, all grades	7.5–15
Ground wood	50–75
Paper—Exclusive of Pulp Preparation	
Paperboard	15–20
Newsprint	15–20
Book	20–35
Kraft	20–35
Glassine	40–50
Bonds	50–60

Typical Plant Requirements

The modern mill requirements include motors from fractional horsepower, for small auxiliaries, to motors of 3,000 horsepower capacity for driving the magazine grinders required in the production of ground wood pulp. The motors may be arranged for direct connected, geared or belted service with horizontal or vertical mounting and for location on the floor, wall or ceiling. Depending on their location and surrounding conditions the motors may be supplied open, semi-enclosed, splash and dripproof, totally enclosed fan-cooled, or totally enclosed forced ventilated. An idea of the motor requirements of a modern mill can be obtained from a recent electrification in a balanced kraft pulp and paper mill having a daily capacity of 600 tons of pulp and paper. The installed motor capacity in this plant is 23,000 horsepower. Power for this plant is provided by 15,000 kw in turbo-generator capacity, which is equivalent to 75 per cent of the installed motor capacity. The motor list includes some 344 induction motors representing a total of 12,300 horsepower ranging in capacity from 1 to 250 horsepower, and some 32 synchronous motors totalling 10,700 horsepower.

Power Generators

The pulp and paper industry in the United States produces the major portion of its total energy requirements and purchases only a minor portion from local utilities. Statistics for the year 1946 indicate that this industry produced 70 per cent of its total requirements as compared with a national average of only 30

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R. R. Baker is with the industry engineering department of the Westinghouse Electric Corporation, East Pittsburgh, Pa.

Figure 1. 250-ton balanced kraft pulp and paper mill

Power is generated and distributed at 2,300 volts. Bleeder-type turbines serve to generate power as a by-product of the process steam

per cent. This situation is undoubtedly due to the fact that in this industry large quantities of power can be generated as a by-product of the steam required for process purposes. To produce each ton of paper requires in addition to 700 kilowatt-hours, from 10,000 to 20,000 pounds of steam at pressures from 25 to 150 pounds per square inch. With boiler plants in the modern mills, operating at steam pressures of 600 pounds per square inch and above and steam temperatures of 700 degrees Fahrenheit and higher, the industry is able to generate a large part of its power requirements as a by-product of the process steam. These possibilities have led to the development and extensive use of the bleeder-type turbine in the industry. Depending upon the operating requirements, these turbine units are designed for either single or double pressure bleeding and for either condensing or noncondensing operation. Units ranging from 1,000 to 10,000 kw capacity are installed. In addition to steam power generation in localities where low-cost water-power sites are available, power is frequently generated at these low-cost sites and transmitted to the mill over high-tension transmission lines.

Sixty cycles appears to be the accepted frequency in the modern mill. Power transmission lines are operated at various voltages, 6.6 and 13.2 kv being quite common. In the modern mill, power is generated at a voltage consistent with plant size and concentration of power as well as with the distribution and motor requirements. 2,400 volts is frequently specified however, with large generating units, and a high concentration of power both 4,150 and 6,900 volts have been used. In plants where the power is generated at 2,400 volts, motors 100-horsepower capacity and over are usually selected for this voltage. Where these motors are required for jogging service necessitating frequent starting and stopping, the use of airbreak contactors for the control are recommended.

Power Distribution

Power distribution is an important consideration in the layout of the modern mill. Power driven equipment is located in numerous rather widely separate areas and it is necessary to provide both high and low voltage power service to each of these areas. It is quite common practice to extend one or more feeder circuits from the central power plant to each of the areas. Power is distributed at the generator voltage and is directly applied to the larger motors at this voltage. Power transformers selected for indoor service and of proper capacity to meet the load demand of each particular area, serve to reduce the voltage for application to the smaller motors. Dry-type transformers provide dependable service for the atmospheric conditions characteristic of pulp and paper mills. Such transformers have a somewhat lower installation and maintenance cost than the conventional oil insulated transformers.

The industry has been quick to recognize the merits of factory built metal-clad switchgear for the important function of power distribution. Such equipment can be properly installed at low cost. The features of this type of switchgear assure safe continuous operation with a minimum of outage time for inspection and repair. Metal-clad switchgear is available for any of the established distribution voltages with either oil or air-break switches, and for either light or heavy duty depending upon the plant capacity and the possible fault currents. Light duty gear is suitable for 25,000 and 50,000-kva interrupting capacity and the heavy duty gear for 100,000 to 500,000 kva. The use of this modern metal-clad switchgear with either draw-out or vertical lift circuit breakers affords maximum safety and assures minimum installation and operating costs.

Many of the mills recently built or now under construction are specifying control centers for both high and low voltage motor control. These factory-built control centers provide power bus, disconnecting switches, electrically operated motor control and both circuit and motor protection. With this arrangement the control may be located at some distance from the motor which it serves and the control functions obtained by means of push button or master switch conveniently located adjacent to the motor. This arrangement permits removing the control from congested manufacturing areas and placing it in areas where it may be properly protected against the adverse

Figure 2. 550-ton bleached sulphite fibre mill

The mill operates a hydraulic barker and a full log chipper

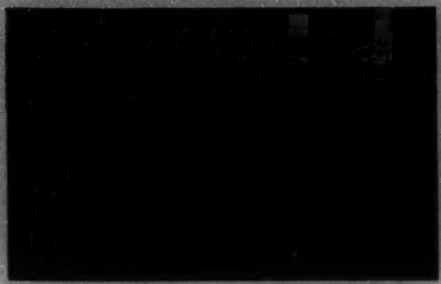

Figure 3. Two 7,500-kw bleeder turbines in the power plant of a large kraft pulp and paper mill

Both turbines take steam at 550 pounds per square inch. Full use is made of by-product power generation

Figure 5. Two power centers typical of units installed in the modern paper mill

Each unit includes a dry-type transformer, high-voltage disconnect switch and low-voltage feeder air circuit breakers

atmospheric conditions frequently encountered in pulp and paper mills. The segregation and grouping of the control possible with these control centers permits the establishment of comprehensive maintenance schedules which are essential to continuous, trouble free, efficient plant operation.

In specifying either the high or the low voltage control centers due consideration must be given to the possible fault currents to be encountered. The recent development of the current limiting fuse for use in conjunction with 2,300 and 4,160-volt motor control has increased the interrupting capacity of this control to 150,000 kva on 2,300 volt service, and 250,000 kva on 4,160-volt service. Fault currents on the low voltage service are usually limited by the reactance of the transformer in the power center serving the area. Such transformers usually do not exceed 1,500 kva capacity and therefore serve to hold faults within 50,000 kva.

General Purpose Mill Motors

Although the industry uses many motor drives which have been designed to meet the special requirements of the individual application, a large part of the total installed motor capacity and by far the largest number of motor units installed in the mills are of the standard squirrel cage induction type. These motors range in rating from fractional to perhaps 200-horsepower capacity. They drive such equipment as pumps, screens, conveyors, and agitators. The control for these motors ordinarily consists of magnetic full-voltage starters providing only "start and stop" control functions. These motors are required to operate 24 hours per day, frequently six and sometimes seven days a week. To a large degree the plant performance is dependent on the uninterrupted operation of this important group of motors. To insure good plant performance such motors must be provided with all necessary protective features such as enclosures, winding insulation motor mounting, and so forth, to meet the operating conditions. These motors must also be carefully maintained to permit continuous uninterrupted service.

Synchronous Motors and Plant Power Factor

By the application of synchronous motors with leading power factor to cer-

Figure 4. Light duty metal-clad switchgear having 50,000-kva interrupting capacity at 2,400 volts installed in board mill

Figure 6 (right). 2,300-volt motor control center serving a group of induction and synchronous motors in the beater room of this paper mill

Figure 7 (left). 440-volt motor control center permitting segregation and remote location of control in this paper mill

Figure 9. Totally enclosed fan-cooled induction motor connected to a pulp thickener through integral gearing

tain drives throughout the mills it is possible for pulp and paper mills to maintain high over-all plant power factor. Synchronous motors are frequently applied to many of the motor generator sets as required for power supply on adjustable speed motor drives, to Jordans and vacuum pumps, to chippers and to grinders. The high operating plant power factor resulting from the proper application of synchronous motors relieves the generators, transformers, switching equipment, and feeder circuits of out of phase current, and thus increases the effective capacity of such equipment.

Electronic and Rotating Regulators

Many developments have recently been made in motor and control equipment for application in the paper industry. These developments include among others, improvements in automatic control and regulating equipment, through the introduction of electronic devices and rotating regulators to meet the more exacting regulating requirements. Although sectional paper machine drive has been an accepted standard for many years, recent developments have improved the reliability and the performance of this equipment.

Fundamentally, a sectional electric paper machine drive includes direct current motors for driving the individual sections of the paper machine, a power set for converting from alternating to direct current and for supplying power to the section motors, control permitting independent operation of the individual sections, and section speed regulating equipment providing for an accurate maintenance of the relative speeds of the various sections of the machine required for the proper formation of a continuous web of paper.

With the modern sectional electric drive the older forms of mechanical differential regulators have been replaced with electronic regulators. As supplied by one manufacturer, this electronic regulator consists of several basic elements. These basic elements include a master a-c reference bus, individual section reference a-c tachometer generators, and individual electronic regulating elements including a vacuum tube mixer circuit, a 2-stage vacuum tube amplifier circuit, and a grid-controlled thyratron rectifier field supply circuit. These new electronic-section speed regulators provide the features of field forcing and antihunting essential to high-speed response. They have an infinite number of regulating points and provide increased sensitivity and stability with a broader control range than former types. After initial adjustment these electronic regulators require nothing more than routine inspection and an

Figure 10. A battery of twenty 350-horsepower and four 150-horsepower synchronous motors driving Jordans for refining pulp in a 400-ton capacity kraft mill

These 80 per cent power factor synchronous motors maintain a high over-all plant power factor

Figure 8. Weatherproof vertical mounted induction motor driving one of seven deep well pumps supplying water to this paper mill

Figure 11. 1,500-horsepower 277-rpm synchronous motor built for direct connecting to full-log chipper

Both the electrical design and heavy mechanical construction of the motor contribute to low-cost high quality chip production

occasional tube replacement to maintain top-notch performance.

The modern sectional electric drive may include a power set with a single main d-c generator for supplying power to the section motors or it may include individual generators for each independent section of the machine. With the single generator the electronic regulators are arranged to control the section speed by control of the excitation of the individual section motors. On the other hand, with multiple generators the regulators control section speed through control of the excitation of the individual generators. Figure 14 shows the arrangement of the regulating equipment when applied with the single generator and Figure 15 when applied with multiple generators.

The auxiliary or helper drive is an extension of the sectional drive principle. D-c motors are applied to certain auxiliary sections of the paper forming machine such as the molds and primary presses of cylinder machines, the primary couch roll, wire return roll and dandy roll of Fourdrinier machines, and top main press and dual press auxiliary rolls on both types of paper machines. Where these auxiliaries form a part of a complete sectional electric drive, the motors are operated in parallel with the main section d-c motors and no further provision is re-

Figure 13. In conjunction with sectional electric drive these eight electronic speed regulators provide precision control of the individual sections of this paper machine

Figure 12. Driving side of a 210-inch Fourdrinier paper machine with an operating speed range of 140 to 1,200 feet per minute showing the use of modern sectional electrical drive

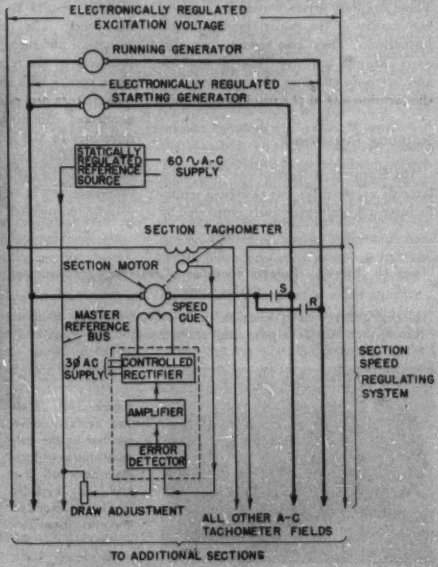

Figure 14 (right). Essential components of a complete section all-electronic regulator applied to single-generator type sectional paper machine drive

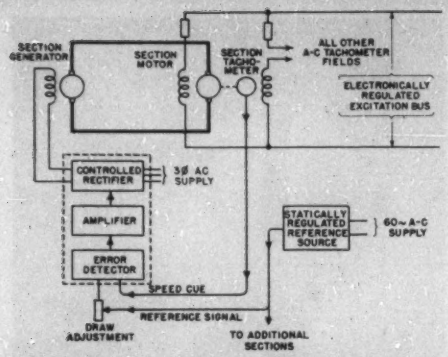

Figure 15. Essential components of a complete section all-electronic regulator applied to multiple generator sectional paper machine drive

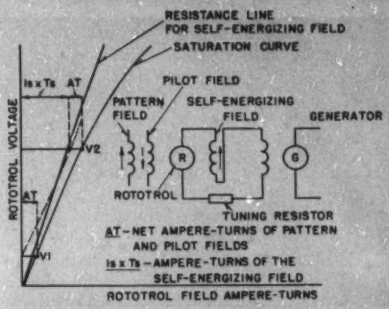

Figure 17. Rototrol saturation curve and armature circuit resistance line illustrating the principle of operation of these units

quired for synchronizing their operating speeds with the balance of the machine. Where the auxiliary drives are applied to paper machines equipped with line shaft mechanical drive, it is necessary to provide a d-c generator which is operated at a voltage proportional to the operating speed of the machine for synchronizing the operations of the auxiliaries with the main drive. The synchronizing generator may be driven from the line shaft or it may form part of an auxiliary power set. These auxiliary drives materially improve the quality of the paper through better formation. They also reduce operating costs through improved felt or wire life and by more effective water removal on the forming end of the machine.

Rotating Regulators

The Rototrol, a form of rotating regulator as supplied by one electrical manufacturer has been extensively employed by the industry because of the advantages it affords in the automatic control and regulation of many of the functions required of the power driven equipment. The Rototrol is similar in design to a standard quick response d-c generator. It has a number of independent field windings which are energized in various ways depending upon the regulating problem. These windings include the pattern, pilot, and self-energizing fields. The pattern field normally sets the pattern for the results to be obtained by the regulator. The pilot field provides a means for measuring the operating results produced by the regulator and comparing these results with the prescribed pattern. The pattern and pilot fields are normally connected differentially and any net difference in the field ampere-turns produces a highly amplified corrective action on the part of the Rototrol to reduce this difference. The self-energizing field is usually connected in series with the armature of the Rototrol and the design is balanced so that the voltage produced by the current in the self-energizing field is just sufficient to overcome the total *IR* drop in the armature circuit. In other words, the *IR* drop line for the armature circuit and the saturation curve of the Rototrol coincide. This is possible when the generator is operated at voltages below the knee of the saturation curve. Figure 17 shows the Rototrol saturation curve and the armature circuit resistance line with the tuning resistance set slightly higher than for maximum amplification to illustrate the principle of operation.

Rototrol presents no more of an operating problem than the ordinary d-c exciter. It is provided with air gaps which are comparable with the standard d-c machine. The brushes are standard commercial sizes and are operated at low-current densities. Not less than two brush arms per polarity are used to maintain a fixed brush drop and the stator frame is usually hydrogen-annealed to reduce to a minimum the effect of residual magnetism.

Figure 16 (below). Rototrol consisting of regulating generator and driving motor

Typical of units providing automatic control of such functions as speed, load or sheet tension in many applications throughout the paper mill

Figure 18. Wet-end auxiliary drive applied to the primary presses and top main press roll of a 142-inch cylinder forming machine improves sheet formation, reduces felt costs

Figure 19. D-c 2-motor adjustable speed winder drive with regenerative braking and automatic sheet tension control

Produces high quality rolls at speeds in excess of 3,000 feet per minute in the finishing room of this mill

Figure 20. 150-horsepower d-c adjustable-speed supercalender drive with Rototrol threading speed regulator

The drive permits maintaining high productive efficiency due to high speed operation with controlled rates of acceleration and deceleration and stabilized threading speed

In pulp and paper mills Rototrols have been applied on paper forming machines to accurately maintain both the operating speed and relative speeds of individual sections of the machine. On supercalender drives Rototrols serve to maintain uniform threading speed and to synchronize the operations of the unwind and wind-up stands with the super-calender. In connection with both reels and unwind stands the Rototrol serves to maintain constant sheet tension as the roll varies in diameter between a full and empty roll. With the primary-couch roll drive as applied on many modern Fourdrinier machines it serves to maintain a motor driving effort consistent with the vacuum maintained in the roll. Applied to wet-end auxiliaries and other helper drives, the Rototrol maintains uniform operating conditions over the wide range of operating speeds. Due to the simplicity of its design the Rototrol presents no more of a problem to the mill operating personnel than a standard exciter unit. However, it provides an accurate and dependable means of automatic control.

Hydraulic Barking

The recent introduction of hydraulic barking in many mills, particularly those located on the west coast, has necessitated the development of a suitable motor drive. With hydraulic barking the bark is ripped from the log by high pressure and velocity jets of water directed tangentally against the surface of the log. Logs as large as 60 inches in diameter and 30 feet long can be handled in a single operation. This method of operation permits a saving in raw material as well as man power, and produces a clean well-barked log for the chipper. In normal operation it is necessary to move either the jets or the log from end to end and at the same time to index or rotate the log in order to completely remove the bark from the entire surface. Motor drives for the hydraulic barkers include power equipment for the nozzle carriages and the log manipulators as well as magnetic control providing complete control for a single operator from a pulpit type control station. To secure a high rate of production with logs of various diameters adjustable speed operation is ordinarily required of both the nozzle carriage and the log manipulators and direct current adjustable voltage drives are specified. Heavy production schedules require frequent reversals and the equipment is selected for this duty.

Full Log Chippers

To take full advantage of hydraulic barking of the large size log, chippers have been developed capable of handling 30-foot logs up to 42 inches in diameter. Such chippers are capable of chipping one of the large logs in less than 30 seconds. Synchronous motors in ratings from 1,000 to 2,000 horsepower have been developed for direct coupling to these chippers. These motors have high pull-out torques required to meet the peak load torques when chipping a full size log. These motors are selected of adequate thermal capacity to meet the normal production rate. They have extremely heavy mechanical construction to withstand the severe vibration resulting from the reaction of the chipper knives passing through the logs, and the damper windings are of special construction to permit acceleration of the high WR^2 of the chipper disk.

Supercalenders

Wide high-speed supercalenders equipped throughout with antifriction bearings for absorbing both radial and thrust loads have replaced older designs in the finishing room of the modern paper mill. These stacks are capable of handling sheets 160 inches in width at speeds in excess of 2,500 feet per minute. The modern supercalender drive provides an adjustable speed main stack drive with both synchronized wind and unwind stands. Such a drive includes a main d-c adjustable speed motor with suitable gearing for connecting to the lower roll of the main stack, a motor generator set for supplying power for the drive, and adjustable speed d-c units for connecting to both the unwind and wind-up stands. These drives also include full automatic control with a centralized control station and metering equipment indicating such operating conditions as speed, and sheet tension between the calender and both the unwind and the wind-up stand. These modern drives provide such operating features as stabilized threading speed, automatic current limit acceleration and deceleration between threading speed and pre-set operating speed, automatic control of sheet tension between calender and unwind and wind-up stands, adjustable speed operation over a wide operating speed range, inertia compensation preventing the loss of tension in the sheet when making speed adjustments, stalled tension, pay-out on unwinding stand, and slack take-up on the wind-up. These various operating features are required to obtain efficient operation of these modern high-speed supercalenders.

Finishing Room Equipment

In the finishing room the adjustable voltage d-c motor drive is suitable for many applications such as winders, past-

ers, corrugators, and waxing machines. Such a drive includes a motor generator set with direct connected exciter, one or more d-c motors for driving the process equipment either as a unit or in individual sections, and complete control for both a-c and d-c equipment. The power set serves to convert the mill power from alternating to direct current for use on the drives. The d-c motors may be operated over the required speed range by armature voltage control, by motor shunt field control, or by a combination of armature voltage and motor shunt field control. Speed adjustments are made by means of rheostats in either the generator or motor-field circuits. These rheostats may be either hand or motor operated. With motor operated rheostats the features of pre-set speed control as well as controlled rates of acceleration and deceleration may be obtained.

In conclusion attention should be called again to the important contributions electrical developments have made in the efficient operation of the mills of the pulp and paper industry. The economic generation and distribution of electric power together with its effective use has resulted in a high quality low-cost product which is required to meet the demands of an ever expanding market. Electric equipment properly selected, installed, and maintained assures many years of efficient operation in the many mills throughout the country.

Discussion

G. W. Knapp (General Electric Company, Schenectady, N. Y.): The comprehensive outline of electrical applications in the pulp and paper industry contained in this paper emphasizes the broad progressiveness of that industry.

Generation and Distribution

In the fields of generation and distribution, the industry is putting into practice forward-looking thinking. High pressures and temperatures are being used on turbines. (For example, 1,200 pounds per square inch and 950 degrees Fahrenheit total temperature on a recent 10,000-kw unit). Voltage levels are being raised for economy, present and future. System grounding is being used in many cases for protection to equipment and faster relaying.

In the application of transformers, many paper mill engineers feel that the dry-type should not be used in those locations where chemical fumes are present as in bleach plants, or where lint is prevalent as around tissue machines, unless ventilated, transformer rooms are provided.

Utilization Equipment

In utilization equipment, Baker's remarks are particularly timely. Electronics and rotating amplifiers (Rototrol. amplidyne, and so forth) have been used in the industry for more than ten years, and today they are given ready acceptance. One manufacturer has installed or has on order almost 400 electronic-amplidyne speed regulating equipments for both sectional and single motor drives. Approximately 200 additional amplidyne controls are being used for winders, super calenders, and wet end helper drives.

Recently, an experienced super calender engineer described a drive which is similar to that discussed by Baker, but with amplidyne control, as the finest he has ever seen

Certainly with such equipment available, and its progressive attitude, the paper industry on this continent can face the future with confidence.

Trends in Electronic Engineering

DONALD G. FINK
MEMBER AIEE

THE FIELD of electronic engineering has emerged in the postwar world as the most active branch of electrical science. Based fundamentally on the conduction of electricity in gases or in vacuum, electronics has come to include many branches of technology formerly classed as separate entities. As an introduction to this review of recent progress in electronic engineering, therefore, it is well to outline the field.

Electronic engineering divides, on technological as well as economic grounds, into three well-defined areas of activity:

1. Entertainment and education.
2. Communications.
3. Industrial applications.

The first classification includes broadcasting by amplitude modulation or frequency modulation, and comprises services for sound (sound broadcasting, motion pictures, phonograph recording, and public address) as well as visual transmissions (facsimile and television).

The second classification, closely related to the first, is concerned with the transmission of information other than for public consumption and includes the fields of telegraphy and telephony, between fixed or mobile stations, by wire or radio.

The third classification, the industrial applications of electronics, includes all parts of the field not related to broadcasting or communications. This field may be defined as the application of electronics in which the essential aspect is the control, guidance, or supervision of a process, or the manipulation or measurement of physical and mathematical quantities to perform an industrial process or commercial service.

Since industrial electronics is by far the most diverse aspect of electronic engineering, it requires a further breakdown. The first division is the guidance of commercial transportation, that is, the use of radio and electronic aids to navigation, traffic control, landing and docking, anticollision, and other safety services. This field has been classified loosely as an aspect of communications technology, as the techniques used in the two fields are similar. But in line with the definitions given above, radar, loran, and other navigational systems are clearly industrial applications.

The second division is the production and control of heat, such as control of resistance welding and the production of heat in conductors or dielectrics by immersion in high-frequency fields.

The third division includes the supervision of processes and protection against hazards. Intrusions alarms and monitors, and protection of operators against hazards presented by machinery are included.

The fourth division comprises continuous process controls. Such continuous processes as printing, wrapping, weaving, plating, rolling, perforating, finishing, sorting, and grading may be controlled or monitored by electronic devices sensitive to some physical quantity, such as weight, thickness, temperature, humidity, angle, position, balance, pressure, color, reflectance, or resistance.

The fifth application involves the manipulation of mathematical quantities, not for control purposes, but for computation. This group includes the many new forms of electronic computers of the digital and analogue types.

The sixth and final division is the large field of electronic measurements, not related to control. Many physical measurements, particularly those involving the perception of minute amounts of radiant or corpuscular energy, cannot be carried out except by photographic or electronic means. The electronic techniques have a considerable advantage over the photographic ones in respect to convenience.

With this brief outline of the field before us, we can proceed to a detailed examination of recent progress in each of the divisions just described.

Trends in Broadcasting

The field of broadcasting commands first attention because it represents by far the largest division of the field from the standpoint of economic investment and employment of technical personnel. Largely as a result of the universal presence of radio receivers in American homes there are, in North America, more electron tubes than there are people.

Considering first conventional sound broadcasting by amplitude modulation, it must be reported that few outstanding technical improvements have occurred in recent years. The principal recent trend in the United States has been along social and economic lines, namely, the very great increase in the number of stations occupying the already overcongested broadcast channels. In December 1945, there were 1,056 amplitude modulation broadcast stations in the United States and its possessions. Two years later, there were 1,961 such stations, an increase of nearly 1,000 stations in 24 months.

These new stations were authorized by the Federal Communications Commission (FCC) following a decision of the United States Supreme Court which, in effect, required the commission to issue licenses without considering the economic status of existing stations. As a result, interference among stations, particularly during nighttime hours, has increased, and this has focussed attention on directional antennas. Many stations have erected 2- and 3-element arrays to reduce the signal strength in direction of

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other stations occupying the same channel. Otherwise there is little change in the service to note. Network telephone-line connections continue, for the most part, to limit audio fidelity at 5,000 cycles per second. Similarly little change has occurred in the design of broadcast receivers, except that higher quality components and better audio systems are used in expensive models, largely because these receivers have facilities for frequency modulation.

In contrast, the progress of frequency modulation broadcasting in the United States during recent years has been marked. Frequency modulation station assignments are made in the band from 88 to 108 megacycles, with a few assignments still active on the older band, 44 to 50 megacycles. Stations in the latter region, notably station *W2XMN* in Alpine, N. J., operated by the inventor of wideband frequency modulation, Doctor E. H. Armstrong, have served as key stations in radio-relay frequency modulation networks which permit high-quality network connections without telephone lines. Telephone lines having an 8,000 cycles per second upper cutoff have also been used in frequency modulation networks, and early this year plans were underway for the use of lines with a 15,000 cycles per second cutoff.

At the end of 1947 there were 376 frequency modulation stations regularly on the air, and construction permits had been issued for 629 additional stations. By the end of 1948 it was predicted that there would be nearly 1,000 frequency modulation stations operating, or about the same as the number of amplitude modulation stations operating in 1945.

Approximately one million frequency modulation receivers were produced in 1947, which represented about one out of every 17 sets manufactured during that year. This represented a sixfold increase in production over the previous year. Frequency modulation receivers continue to be expensive, few table models selling at under 50 dollars. One step in the direction of cheaper frequency modulation sets is a new one-tube frequency modulation circuit combining the functions of superregeneration and the superheterodyne. While the performance of this simple circuit leaves much to be desired, it permits a combined amplitude modulation–frequency modulation table model set to be marketed for $30.

More rapid popularization of the frequency modulation service has been prevented by the action of Caesar Petrillo, head of the American Federation of Musicians, who refused to allow programs broadcast of the standard amplitude modulation stations to be broadcast simultaneously over frequency modulation stations without payment of double fees to the musicians. Consequently programs on frequency modulation stations consisted almost exclusively of phonograph records, and the listening audience was small. The Petrillo edict was withdrawn on February 1, 1948, and a steady increase in the frequency modulation audience since has occurred.

The power levels of frequency modulation stations, in the 88-to-108 megacycle band, range from 250 watts to 10 kw. Several 50-kw transmitters are in operation in the lower band, 44-50 megacycles. High-gain antennas, which compress the signal in the horizontal direction, are usual, producing an increase in effective radiated power of from two to six times. The effective range of a high-power transmitter with a high gain antenna is nearly 100 miles, if the receiver was located favorably although reception at ten miles may be unsatisfactory if large obstructions intervene.

Perhaps the most rapid progress of all, in the field of broadcasting, is that made by television. Permission to operate television stations commercially first was granted by the FCC in July 1941, but construction of new stations was halted immediately by the war. At the war's end, seven stations were in operation. At the end of 1947, 16 stations were on the air in 11 cities. Construction permits for 56 additional stations in 32 additional cities had been granted, and applications for 84 stations were pending before the FCC. In 1946, some 6,500 television receivers were manufactured; in 1947 this production had risen to 178,000. One large manufacturer reported that nearly half of the income derived from manufacturing radio receivers was accounted for by television sets.

The first television network took form along the Atlantic seaboard in 1947, connecting ten stations in Washington, Baltimore, Philadelphia, New York, and Schenectady. The coaxial cable, with a band width of 2.7 megacycles serves from Washington to New York. Microwave radio relays, operating at frequencies in the thousands of megacycles, connect New York, Philadelphia, and Boston. During a demonstration of the microwave relay system between New York and Boston (220 miles long), a television program was repeated back and forth over the system four times, or a distance equal to that from New York to Chicago, without visible degradation of the picture quality. Construction of a similar microwave relay between New York and Chicago is underway.

Television receivers range in price from $175 to $2,500, and produce pictures ranging in size from 4 by 6 inches to 15 by 20 inches, for home use. The larger size pictures are produced by a Schmidt projection system which enlarges the image from a small (4- or 5-inch) cathode-ray tube. Directive viewing screens for projection receivers enhance the image brightness by a factor of ten times, and permit satisfactory viewing in a sun-lit room. Of the 13 channels allocated by the FCC, no more than seven are assigned to any one locality. All commercial receivers have facilities for receiving any of the seven local stations, and many receivers provide tuning for all 13 channels. Picture brightness (in the highlights of the scene) of 50 foot-lamberts is achieved, with a contrast range as high as 100-to-1. The detail of the images varies from 100,000 to 150,000 picture elements per frame, depending on the excellence of the television camera in use and the effective bandwidths (three to four megacycles) employed by the receiver. The higher figure approximates closely the detail available from 16-millimeter motion pictures. The images are sent in a 525-line scanning pattern at a rate of 30 complete pictures per second.

Television for theaters is under active development. Images of six by eight feet were demonstrated at the International Telecommunications Conference in Atlantic City in the summer of 1947.

Among the most important of the recent technical developments in television is the image orthicon camera tube. This tube, which combines the properties of photoelectric emission, charge storage, and electron multiplication, is the most sensitive continuously registering light-sensitive device known to science. This camera will register an image under the illumination of a single tallow candle six feet from the subject. Such dim light will not actuate photographic film, when the same exposure (about 1/30th second) is used. In fact, the effective photographic speed of the image orthicon camera, for threshold values, is about Weston 2,000, compared with Weston 200 for the fastest photographic film.

The other form of visual broadcasting, facsimile, is ready for use, but has not been introduced to the public to any extent. Standards of transmission have been agreed upon by the industry and presented to the FCC, and experimental transmissions have occurred on a number of frequency modulation broadcast stations (being transmitted by multiplex

methods simultaneously with the f-m sound program) but facsimile equipment for the home is not as yet available and it appears that further co-ordinated effort by receiver manufacturers and broadcasters is required before the service becomes established. Facsimile service for communication purposes, particularly the transmission of news pictures and business documents, is well established at present.

Sound Reproduction

Widespread use of electronic techniques is made in entertainment and educational pursuits other than broadcasting. The sound aspect of motion pictures is a typical example. Since the advent of ultraviolet recording and the perfection of light-valve characteristics, little technical progress has been reported in this field. But in other types of sound recording, very rapid strides have been taken in recent years. In conventional disk recording the employment of better materials (particularly vinylite plastics) for pressings has resulted in a marked improvement of the tonal range, as high as 14,000 cycles in current British records, and a great reduction in the background noise inherent in the records.

An important and technically interesting improvement in the reproduction of disk records is the dynamic noise suppressor. In the form of suppressor developed by H. H. Scott, the tonal range of the reproduction is varied automatically and continuously, in response to changes in the volume of the recorded sound. Thus when the volume is low, the frequency range is restricted by an electronically controlled wave filter. This restriction removes noise, but does so only when the volume is low and the noise would be prominent if not removed. Since the tonal range to which the ear responds is naturally restricted at low volume levels, the tonal restriction is not perceived to any great degree. During loud passages of the music, the full tonal range is reproduced, as is required by the ear at high volumes, but the noise then is masked by the music and is not perceived. Such suppressors produce an amazing improvement in apparent signal-to-noise ratio on musical recordings, without audible degradation of the tone quality.

Another area of high activity in sound reproduction is the development of wire and tape recorders. The early forms of wire recorder had rather restricted tonal range, but reproduction faithful to 8000 cycles has been achieved on wire, and up to 13,000 cycles per second in tape recorders. The latter, following German practice, employ a finely-divided iron-powder emulsion deposited on a cellulose tape base.

Trends in Electronic Communication

Turning now to the field of communication by telegraphy, telephony, television, and facsimile, we encounter first an important addition to the theory of communication which has important practical implications in trunk-line telephony by radio relay. The new theory relates to the bandwidths required to transmit a given amount of information in a given amount of time. The classical statement of this relationship, the Hartley law, states that the minimum bandwidth, required in a communications channel to transmit information at a given rate, is equal to the spectrum occupied by the information itself. Thus, the band of frequencies occupied in commercial telephony extends from 250 to 2750 cycles per second. An ideal single-sideband system would require a frequency band at least 2,500 cycles per second wide to transmit this signal. If transmitted by double-sideband amplitude modulation, the band required is at least 5,000 cycles per second, and if wideband frequency modulation with a modulation index of 5 is used, the required bandwidth is at least 25,000 cycles per second.

The new theory takes into account the noise present in the transmission channel and states that the minimum bandwidth required is proportional to the quantity $\log (1+S/N)$ where S/N is the signal-to-noise power ratio in the transmission medium.

This law, derived independently by workers in the Bell Telephone Laboratories, Massachusetts Institute of Technology, and California Institute of Technology, has rather startling implications. The most important of these is that, if the signal-to-noise ratio in the transmission path may be made arbitrarily large (by employing an unlimited amount of power at the transmitter), the bandwidth required for full and accurate reproduction of the information may be made arbitrarily small. Thus, if resources are available to keep the transmitter power sufficiently high at each repeater point in a telephony, telegraphy, or television system, the bandwidth of the transmission system may be reduced below the extent occupied by the spectrum of the original intelligence. For example, a television signal, extending from 30 to 4,000,000 cycles per second as produced by the television camera, can be sent over a co-axial cable or radio relay system, having a bandwidth of only 2,000,000 cycles per second provided the signal-to-noise ratio is kept suitably high at each repeater point.

To achieve this important economy of spectrum utilization, a particular method of modulation must be used, the only practical form of which, now known, is "pulse-code modulation," described in the following. Although this system is probably not practical for broadcast use because of economic factors, it is quite possibly the optimum system of transmitting high quality telephone signals over restricted bandwidth in trunk service. Active investigation of the practical application of the new theory to telephone plant is now underway in several laboratories.

The second important trend in communication practice is employment of different modulation methods. Amplitude modulation continues to predominate in long-distance telephonic communications, and a modification of the amplitude modulation system in the form of single-sideband suppressed-carrier increasingly has been employed in long distance circuits, and has most recently been the subject of intensive activity among amateur radio telephone operators. Frequency modulation has been used widely in communications work, particularly the local services using frequencies from 30 megacycles upwards. A special form of frequency modulation system, frequency-shift keying for telegraphic circuits, has increasing vogue in long-distance work. Frequency modulation even has been proposed for international (transoceanic) broadcasting, but the wide band (about 200 kc) which is required for this service probably cannot be made available even for experimental work in the overcrowded bands between 5 and 25 megacycles.

For high-frequency carrier services (300 megacycles and higher) pulse methods of modulation actively are being developed and are employed to some extent in commercial practice. The two most prominent methods of pulse modulation are pulse time modulation and pulse-code modulation. In the first, a series of flat-topped steep-sided signals (pulses), of duration small compared with the interval between them, is set up as the basic transmission system. The intelligence to be transmitted is imposed on this sequence of pulses by varying the time of occurrence of the pulses in the sequence. This is, in effect, a phase modulation of the pulse, and the modulation may be recovered by passing the pulse sequence through a phase detector. This system has the

advantage that the transmitter and receiver need not be linear, as amplitude variations above threshold are of no consequence. Moreover, the pulses may be regenerated at repeaters by trigger action. The bandwidth required for pulse time modulation is generally more extensive than for amplitude modulation or frequency modulation but, at the high carrier frequencies for which the system is intended, sufficient ether space to accommodate the wide band is currently available.

The pulse-code modulation system, previously referred to, has important advantages over pulse-time modulation in that it permits a marked reduction in bandwidth if sufficient power is used. In the pulse-code system the amplitude coordinate of the signal wave is "quantized," that is, several discrete amplitude levels are set up as index points. The wave amplitude is then sampled at regular intervals of time, and the discrete amplitude level most nearly approached at each instant of sampling is translated into a code group, consisting of pulses of equal height, spaced in accordance with a binary code. The pulse code groups are transmitted and at the receiving point, each pulse group is retranslated into the corresponding amplitude level and the original signal wave thus is recreated in quantized form. If the number of amplitude steps set up is sufficiently great, the quantized nature of the reproduction is not noticeable. A binary code of 7 characters permits $2^7=128$ levels to be used and this structure is fine enough to permit reproduction of high quality speech and music; 32 levels suffice for commercial telephonic quality. The system employs regeneration of pulses at repeaters, a process made simple by the synchronous recurrence of the code pulses.

A third trend in communications practice is the great extension of radio facilities for communication in industries outside the common-carrier telephone and telegraph companies. As of December 1947, there were 877 communication stations licensed to public utilities, 972 to operators of busses, trucks, and taxis, and several score of stations to transit utility, petroleum pipe line, highway maintenance, and geophysical prospecting companies. The majority of these communication systems employ frequency modulation in the vicinity of 30-40 and 153-160 megacycles. Amplitude modulation also is used in these bands, but no more than two per cent of the stations employ this type of transmission.

A communication service still in the developmental stage, but promising much for the future, is the Citizens Radio Service. This service was established by the FCC in 1945, in the band between 460 and 470 mc (three-quarter meter wave length) for the use of any citizen, not possessing technical qualifications, for private or personal communication. Equipment for this service is not yet available to the public, but is under development. When available, any citizen may obtain authorization to use approved equipment for any private purpose, merely upon application, stating that he understands and will uphold the laws affecting radio communications.

A final development in the field of communications is the great extension of the ether spectrum now available. Prior to the war the spectrum allocated by the FCC extended upward to 300 megacycles, and services were not in use above 200 megacycles. Following the war, largely as a result of the wartime development of radar, the allocated spectrum was extended by the FCC to 30,000 megacycle (1 centimeter wave length). Communications system now in daily use occupy channels in the 2,000 and 4,000 megacycle range. Noteworthy in this category are the microwave relay systems, previously mentioned, for transmitting telephone and television signals from city to city in common carrier service.

Industrial Applications—Radio Navigation

We turn now to the industrial applications, and begin with one of the most important applications of electronic techniques to commerce, the guidance of aerial and marine transportation. Prior to the war this field was limited to the radio direction finder (direction-finder loop and Adcock aerial) and the radio-range course-marking beacon. But with the war came the active employment of several new radio aids to navigations, notably radar, hyperbolic navigation, and radar beacons. The principle of radar is by now well known. In the so-called nonco-operative type of radar, the radio wave sent is reflected by an otherwise passive object which differs from its surroundings. The direction and distance of the reflecting object are determined by directing a narrow beam toward it and timing the wave during its flight to and from the target. The resulting "echo" reflections may be displayed for visual inspection on a cathode-ray tube.

The most useful form of radar display takes the form of a map-like presentation, the center of the screen corresponding to the location of the radar, with reflecting objects shown at their proper relative distances and directions from the center. This "plan position indicator" now is widely employed as an aid to marine navigation, as it shows clearly the location and shape of coastlines and other landmarks useful in pilotage, and reveals the presence and courses of other ships. As such it serves not only as a navigation aid but as an anticollision device. Ships equipped with such radar equipment now may run with confidence, and even may negotiate narrow rivers, in fog or darkness. Current developments may produce a radar so rapid in its perception of echoes that it can "see" objects only a few tens of feet away and thus permit docking under conditions of poor visibility.

Although similar radar devices have proved of the utmost value in military aviation, they have not been applied to any great extent to commercial air navigation. Rather, radar has been applied to aviation from the ground, notably in the "ground-control approach" system (GCA). This system locates the position of an aircraft near an airport by a ground-based radar, and this information is relayed to the pilot by radio-telephone. In this manner the pilot may be guided into proper position for a landing and "talked" down to within a few feet of the ground. Air-borne radar is by no means a dead issue for commercial aviation. Recently a new lightweight radar (type *APS-42*) has been developed specifically for commercial aircraft.

A closely related system is the co-operative type of radar, in which specially-designed receiver-transmitters (radar transpondors) are set out as beacons. These receive the outgoing signal from a shipborne or airborne radar, and return a coded reply signal which is displayed, along with natural reflections, on the plan position indicator. In this manner the pilot of the radar-equipped vehicle can identify positively landmarks which otherwise might be unidentifiable or escape detection. One version of the radar beacon system is the distance-measuring equipment (DME) now being installed by the Civil Aeronautics Authority for commercial air line operation.

Still another specialized adaptation of the radar beacon is the shoran system, which times the reply signal from a beacon so precisely that the distance from aircraft to beacon can be measured to an accuracy of five yards, over distances of several hundred miles. Used for precision bombing during the war, shoran finds postwar application as an aerial survey instrument. Accurate charting of regions of the world by shoran has

revealed errors of many miles in charts at locations (notably the Caribbean) where local disturbances in the gravitational field have prevented accurate celestial measurements of position.

Another war-developed navigational aid of continuing peacetime use is the loran system. In this system stations on islands or coastlines transmit accurately synchronized pulses, broadcast over several hundred thousand square miles of the adjacent sea. The navigator of a loran-equipped ship or aircraft observes these pulses in pairs on a cathode-ray screen in such a way that he can determine the difference in the time of arrival of the two sets of pulses at his location. As the time difference between the pulses emitted by two stations is a constant along a hyperbolic locus projected on the earth's surface, measurement of this time difference suffices to locate the navigator on one of a family of such loci, which are printed on charts of the region. Similarly measurements on a second pair of pulses from two other stations (one of which may be common to the first pair) places the navigator on another locus, intersecting the first. The point of intersection, which represents the navigator's position, can be determined to an accuracy of a few miles at distances as great as 1,500 miles from the stations.

At the end of the war, nearly one third of the earth's surface was covered by loran hyperbolic coordinates, and a large portion of this service is still maintained by stations operated by the United States Coast Guard and agencies in other countries. Nearly 1,000 transoceanic flights now are completed each month by loran-equipped aircraft and scores of merchant vessels, in addition to all naval vessels and many military aircraft, use the system.

Many other navigational systems are under development, most of them aimed at the as-yet-unsolved problem of controlling dense airt raffic in the vicinity of airports. These combine radar, radar beacons, television, and radar altimeters (automatic height finders). The latter device is a radar which measures the distance of the aircraft above ground by timing the interval of reflection of a signal sent from the airplane after it strikes the ground and returns to the aircraft.

Electronic Production of Heat

Electronic heating by the immersion of substances in high frequency fields has grown in importance very substantially in recent years. In view of this increasing use, and in view of the severe interference to communication and broadcast services which may be caused by industrial heating equipment, the FCC in 1948 set up three frequency assignments for "scientific industrial and medical" equipment. These assignments consist of bands, centered on 13.56, 27.12, and 40.68 megacycles. All medical diathermy and industrial heating equipment which cannot be shielded adequately must employ a frequency in these bands.

The principal trend in this field is the use of higher frequencies and higher powers. Frequencies in the thousands of megacycles have been employed to preheat high-quality plastics material prior to molding, to defrost frozen foods, and to cook foods. Continuous-wave magnetrons are employed to generate several kilowatts of power at these ultrahigh frequencies, the energy being conducted through waveguides to a chamber containing the material to be heated. Frequencies in the 10-30 megacycle range have been employed widely to set glue, notably in the furniture and plywood industries. Drying of rayon cakes is also an important potential application.

Somewhat lower frequencies are used in metal hardening, brazing, and soldering operations. One of the largest units in metallics service is a 200-kw generator, operating in the hundreds of kilocycles, used to flow tin in the tin plate industry.

An allied technique, not involving the production of heat, is the preservation of foodstuffs by passage of cathode rays through the food. A new electron tube, the capacitron, operates at five million volts and produces an intense beam of cathode rays, limited in duration to a few microseconds. So intense are these rays that they emerge through a thin window at one end of the tube and may be passed directly through food or other perishable substances. The high intensity of the bombardment, coupled with its short duration, excites a preservative radiation within the foodstuff without producing sufficient heat to induce chemical changes. The effect of the bombardment, in the majority of foods thus far tested, is to kill bacteria and other micro-organisms, and to inhibit the activity of enzymes to such an extent that food will remain fresh for months without refrigeration, and in fact with no treatment whatever except cover to prevent loss of moisture. Meat and poultry, fats and oils, vegetables (except lettuce, which wilts under treatment), fruits, and perishable drugs have been treated successfully. The treatment may be applied to food in any form, dried, frozen, raw, partly or fully cooked, since no chemical change is produced by the passage of the cathode rays.

Trends in Process and Machinery Controls

Among the application of electronic controls to machinery and processes, is a notable trend toward the use of thyratron motor controls on small machine tools. Technically this device is not new, but its extensive penetration into the metal-working industries is of recent date. The motor control serves to give smooth control of motor speed and torque without loss of efficiency.

Noteworthy among recent continuous process controls is a register control which combines an electronic computer and a hydraulic mechanism for correcting the register between successive imprintings of colored ink on paper or metal. The over-all performance of this register control is outstanding: register is held with an error on 1/1,000 inch while the printed material is passing through the press at a speed of 1,000 feet per minute. Register marks printed by the first color are viewed by a photo tube which develops an electric pulse of current as each register mark passes. These pulses are shaped and passed to a computer unit which determines the rate of passage of the marks and detects any difference in the rate relative to the printing speed of the succeeding color. Immediately that such an error is detected, it is amplified to the level of about 25 watts, and actuates a hydraulic valve which drives a differential connected to the printing rolls. The production of a pressman operating a press fitted with this register control is tripled. As the electronic components must operate, in certain applications, in an explosive atmosphere, all tubes and components are immersed completely in oil and sealed hermetically.

Another continuous process control of recent date is the X-ray thickness gauge, used to monitor the rolling of hot-rolled steel sheets. Since the sheet is very hot when rolled, simple contact gauges have short life and are inaccurate at best. An X-ray gauge, which does not touch the sheet, operates as follows: an X-ray tube on one side of the sheet irradicates it with rays of intensity just sufficient to penetrate the sheet and cause a fluorescent screen on the other side to emit light. This light is viewed by a photoelectric tube in a balanced circuit; the output of the photoelectric tube is thus sensitive to minute changes in the fluorescent light, such as are caused by very slight changes in the thickness of the sheet. The photoelectric tube current indicates to the mill operator the extent of the thickness variations, and even may be used to maintain

the thickness automatically within desired limits.

An application of electronics in the aviation industry, capable of similar savings in costs of operation, is the electronic aircraft engine analyzer. This is a cathode-ray oscilloscope connected to the ignition harness of an aircraft engine in such a way that the voltage across each of the spark plugs individually is represented as a function of time. The resulting wave form is an accurate index of the excellence of the ignition process and in particular will indicate impending ignition failure before it occurs. When so detected, a faulty spark plug may be identified and replaced in a very short time, often while the aircraft is making a scheduled stop. Without the engine analyzer faulty ignition cannot be detected in advance, and the time lost in finding a faulty plug may cost several thousand dollars in revenue lost while the aircraft is out of service.

Among the telemetering devices, one of the most interesting is the application of television to the monitoring of water level in high pressure boilers. To safeguard such boilers, it is customary to install complicated systems of mirrors, and safety laws require an operator to watch the water-level tubes at all times. Extensive research has proved that it is difficult, if not impossible, to replace the mirror system with a less cumbersome telemetering system, because the telemetering system might give a false indication and thus place lives and property in grave jeopardy. A satisfactory alternative was found in television, a television camera to view each of several water gauges and a group of viewing screens so arranged that a single observer can monitor several units simultaneously. Television proved satisfactory because it cannot give a wrong indication; it either shows the gauge level properly or it shows nothing at all. In event of failure of the television system, of course, rapid recourse must be taken to the mirror system, which is installed as an auxiliary to the television system.

Electronic Computers

As a concluding classification, we shall consider devices which are perhaps the most significant of all electronic devices now in process of development, the electronic computers. By making possible rapid computations from huge masses of data, problems formerly impossible of solution now may be solved. One possible result is accurate long-range weather forecasting. Another is the computation of all sorts of mathematical tables, from Bessel functions to nautical almanacs, in weeks rather than years.

Two types of electronic computer have merged in recent years, the analogue computer and the digital computer. The analogue type is a combination of electronic circuits which is in effect on electronic model of the device or process whose operation is to be computed. Circuits capable of adding, subtracting, multiplying, dividing, differentiating, and integrating are known and may be so combined as to perform a sequence of operations producing directly a variation of current or voltage which represents the desired solution. These computers have the advantage of comparative simplicity, but are limited in the accuracy with which they can compute.

The digital computer is, in essence, a counting device capable of extremely rapid action. As all mathematical operations may be reduced to a series notation which is, in effect, a counting process, the digital computer is of extremely wide application and its accuracy may be extended to any desired limit by multiplying the number of counting units. The Eniac computer, one of the first of the digital type, employs 18,000 tubes. A more recent embodiment of this principle, the International Business Machines computer, employs 12,500 tubes and operates with almost unbelievable speed and range.

The basic mathematical data are introduced to this machine by punched cards, and similar cards are employed to set up the sequence of mathematical operations. Numbers of 19 digits may be accommodated, and operations may occur at the rate of 24,000 digits per minute. Intermediate totals in a computation may be stored electronically, in electromechanical relays or punched tape, the total storage capacity being 400,000 digits. Using this computer, the position of the moon, an important element of nautical almanacs, may be predicted for any time for hundreds of years in the future, in seven minutes of computing time. The mathematical process involves summing a series of 1,600 terms, that is, performing 9000 multiplications, 10,000 additions and subtractions, and taking 1,800 values from trigonometric tables.

With such comprehensive mechanisms available, it is certain that vastly complicated processes of nature, particularly those now opening up in nuclear science, soon will be capable of analysis. With the understanding such analysis will provide, we may look forward confidently to a broader and more comprehensive technology.

No Discussion

Power Development in Mexico

ALEJANDRO PAEZ URQUIDI
ASSOCIATE AIEE

ALFONSO FERNANDEZ DEL BUSTO
MEMBER AIEE

GERMAN GARCIA LOZANO
NONMEMBER AIEE

AT the Southwest district meeting held at San Antonio, Texas, in April 1946, the Mexico Section of the AIEE presented a paper entitled, "Mexico's Electrification Program," in which an analysis was made of the estimated requirements to supply the increasing demand of electrical energy up to the end of 1949. Although only two years have passed since that meeting, it may be of interest to review briefly the results achieved in a problem so vitally important for the industrialization of the country.

The previous paper explained the urgency of increasing the installed generating capacity by 53 per cent in the following four-year period and that, in order to accomplish this, new generating plants with a total capacity of 400,000 kw were required. The statistical information gave our installed capacity for 1945 as 756,000 kw, and therefore, in accordance with that paper, a total capacity of 1,156,000 kw should be available by the end of 1950.

The results obtained during the last two years, that is up to the end of 1947, are as shown in Table I. It would appear that the forecast made for 1947 was on the high side. However, it should be taken into consideration that the generation in 1947 was below what should have been generated due to delays in the delivery of power plant equipment that had been ordered in the United States, and which were caused by the strikes that handicapped the majority of the industries in that country. This situation was further aggravated by an extraordinary shortage of water during the dry season at the beginning of 1947 that affected the generation in three of the most important hydroelectric systems supplying over 80 per cent of the total production in the country. As a consequence, the generation in these systems had to be reduced and restrictive measures on the use of electric energy were imposed in several of the most important zones of the country. In some places, the use of energy was restricted to 50 per cent of the normal consumption.

Table I

	Kilowatt-hours
Energy generated during 1945	3,068,000,000
Energy to be generated during 1946 as per forecast made early that year	3,460,000,000
Actual generation during 1946	3,317,000,000
Energy to be generated during 1947 as per forecast made early in 1946	3,840,000,000
Actual generation during 1947	3,598,000,000

The energy (in kilowatt-hours) that could not be generated for the foregoing reasons is estimated as follows:

The Mexican Light and Power Company, Limited, and subsidiaries	70,000,000
Nueva Compañia Electrica Chapala, S.A., and subsidiaries	33 000,000
Guanajuato Interconnected System	28,000,000
Total	131,000,000

Had it not been for these reasons the actual generation in 1947 would have been only 2.9 per cent below the forecast made in 1946.

Figure 1 gives the generation up to 1947 inclusive and the probable generation for the period 1948–1950.

If the forecast as regards generation was on the high side for 1947, that made on the installed capacity was surpassed because at the end of last year the installed capacity reached 963,000 kw, showing an increment of 207,000 kw in two years as compared with the increment of 400,000 kw considered as necessary in 1946 for the following period of four years. It should be borne in mind that a large portion of the additional capacity came into service in December 1947, this being the reason why this additional capacity could not give a proportional increase in the generation.

If the generation, in addition to the imported energy, and the installed capacity in 1945 and 1947 are compared, respectively, with the population recently estimated for these same years, the indexes shown in Table II are obtained.

In order to meet the increasing demand, consequent to the industrialization program of the country, new power plants and additions to existing ones are in process of construction and it is expected that 359,900 kw in additional generating capacity will be available in the next three years.

The plants now in course of construction are seen in Table III. Other projects for new plants and additions to existing ones, for a total capacity of 700,000 kw, are being studied and it is expected that the majority of them will be in service by the end of 1952.

As an interesting instance, among the plants in process of construction may be mentioned a 30,900 kw diesel-electric power plant being erected in the Federal District by the Cía Mexicana Meridional de Fuerza, S.A. The engines of the six units to be installed will be the largest ones built up to the present time in the

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ALEJANDRO PAEZ URQUIDI is executive director, Comision Federal de Electricidad, Mexico, D. F., Mexico; ALFONSO FERNANDEZ DEL BUSTO is with the Mexican Light and Power Company, Limited, Mexico, D. F., Mexico; and GERMAN GARCIA LOZANO is chief of rate department, Nueva Compañia Electrica Chapala, S.A., Mexico, D. F., Mexico.

The Institute is indebted to the Mexico City Section for the translation of this paper from Spanish into English.

Table II

	1945	1947
Population (estimated)	22,226,000	23,219,000
Installed capacity, kilowatts	756,000	963,000
Generation, kilowatt-hours	3,068,000,000	3,598,000,00
Imported energy, kilowatt-hours	59,000,000	89,000,000
Total energy, kilowatt-hours	3,127,000,000	3,687,000,000
Installed capacity per capita, watts	34	41.5
Kilowatt-hours per year per capita	140	158

United States. Each engine will have 12 cylinders, 29 inch bore, and 40 inch stroke, with a brake horsepower rating of 8,650 horsepower at sea level and 7,325 horsepower at 2,257 meters altitude. It is expected that this plant will start operating by September 1948.

As was pointed out in the paper presented at the meeting in San Antonio, Texas, not more than 27.5 per cent of the total population of the country is concentrated in cities of 5,000 or more inhabitants and not more than 20 per cent of the population benefits from electric service, showing that the majority of the population is scattered in rural zones without electricity.

These sparsely populated rural centers cannot offer an incentive for electrification to the privately-owned utilities because, in the majority of cases, they cannot expect a reasonable return on their investments, mainly during the first stages of the development. This has really created a vicious circle. The meagerness of the market does not justify, economically, the installation of a power plant and the lack of such a plant prevents the expansion of the zone. In order to break this circle, and considering the problem from a social viewpoint rather than its lucrative end, the Federal Commission of Electricity has undertaken a vast program of rural electrification and expects that by carrying it out the economical situation of these zones and the standard of living of the population will be greatly improved.

To illustrate how this program has been planned, a brief description of the work being done in the State of Veracruz may be of interest.

As a first step, a field survey was carried out of all villages with more than

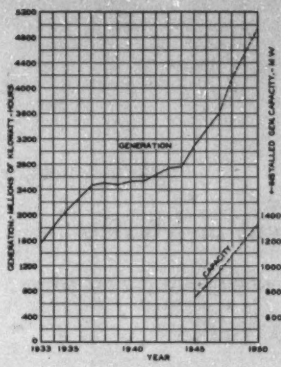

Figure 1. Total power generation and installed capacity

1,500 inhabitants lacking electric service or with such a deficient one that it could not be considered as a "public service." A topographical map was made of each village, classifying every house by its category and indicating for each the probable load. A similar census was taken of all the industries operating with small plants and of those now using hand labor exclusively but which could be electrified. In this way, a fair estimation of the requirements of each village and of its surrounding agricultural district was made.

After spotting these villages on a map of the state, those relatively close to each other were grouped together to form small isolated systems, each to be provided with a Diesel power plant and the necessary rural type distribution lines.

The villages were grouped in such a way that later on, depending on the power facilities and the demand, some of the groups can be interconnected to make larger systems until a stage is reached when the erection of transmission lines joining together all these groups becomes justifiable from an economical standpoint.

Every time that two or more of the initial small groups are interconnected a larger plant will be provided to feed the larger system thereby reducing gradually the number of small plants.

A standard type of power plant has been selected for the initial steps of the

Table III. Plants Now Under Construction

	Thermoelectric Kilowatt Capacity	Hydroelectric Kilowatt Capacity
Sta. Bárbara, Mexico		63,600
Tacubaya, Federal District	30,900	
Extension to Lerma, Michoacán		27,600
Colimilla, Jalisco		25,600
La Laguna, Durango	25,000	
Guaymas, Sonora	25,000	
El Durazno, Mexico		16,400
Extension to Necaxa, Puebla		16,000
Extension to Bella Vista, Nuevo León	15,000	
Monclova, Coahuila	10,000	
Ciudad Juárez, Chihuahua	15,000	
Guadalajara, Jalisco	10,000	
Ciudad Obregón, Sonora	15,000	
El Encanto, Veracruz		10,000
Minas, Veracruz		9,600
Celaya, Guanajuato	7,500	
Puebla, Puebla	7,500	
San Luis Potosí, S.L.P.	7,500	
Other small plants	13,500	9,200
Totals	181,900	178,000
Grand Total	359,900 kw	

program, consisting of diesel units of from 100 to 300 kw. In order to provide adequate maintenance, without affecting the continuity of service, each plant will have a complete spare generating unit.

When selecting the equipment, special attention was given towards standardization, not only for different units of the same plant but also for the various plants that may be installed all over the country.

This will permit reducing to a minimum the investment in spare parts, giving at the same time more flexibility to the use of such parts and a more ready and reliable maintenance.

Another advantage of this standardization is that the operating and maintenance personnel will have to get acquainted with only one type of equipment and will gradually acquire a higher degree of specialization.

In the State of Veracruz, selected as an illustration of this program, 76 villages with a total population of 166,000 inhabitants have been grouped into 36 small systems, each to be provided with a power plant of 200 to 900 kw; the total plant capacity to be installed is 14,400 kw.

This brief outline shows the effort made by the Mexican Government and the cooperation given by the privately-owned public utilities to carry through the program of electrification so important for the welfare of the country.

No Discussion

Primary Detectors for Measurements

E. E. LYNCH
MEMBER AIEE

A. J. CORSON
MEMBER AIEE

The basic elements of any measurement system may be classified into three functional groups known as primary detectors, intermediate means, and end devices. By classifying the primary detectors for both electrical and nonelectrical quantities, convenient selection of a suitable detector may be made when the quantity to be measured together with the range over which it will vary is known.

IN VIEW of the growing importance of measurements to technical progress, it is surprising that only limited attention has been given to a complete functional analysis and classification of the whole measurement field. One excellent step in that direction has been made. In essence, this consists of classifying the basic elements of any measurement system into three functional groups known as the primary detectors, the intermediate means, and the end devices.[1-3] These groups are shown schematically in Figure 1 and each group may be composed of one or more sets of measurement components called basic elements. Each basic element may operate with others in series or in parallel or in any complex manner.

This article is written as the result of a more detailed study of the functional groups just mentioned and will lead to a more complete realization of the advantages of the over-all analysis. A method of treating and analyzing one particular group of any measurement sequence is presented in a novel and useful manner. The first group, the primary detectors, is discussed in this article. This investigation results in a method of classification which is suitable for choosing the primary detector best adapted for measurement of a particular quantity. It serves as a guide toward effectual further study of obtainable accuracies and it indicates other detectors which might be used as substitutes. It also presents a standardized method of analyzing any primary detector, a method which will result in a practical understanding of both the steady state and transient operation of this part of the measuring system.

The primary detector in a measurement system has been defined as the first basic element or group of elements that responds quantitatively to the quantity measured and performs the initial measurement operation. For example, in a simple measurement of radio-frequency current, it consists of a thermocouple converter as shown in Figure 2. In a more complex system, the detector may be a separate device such as the a-c generator of a railway tachometer, as illustrated in Figure 3. Moreover, a primary detector may be a relatively simple unit such as a thermocouple, or more complex as in the case of a watt-hour meter mechanism.

CLASSIFICATION OF PRIMARY DETECTORS

Applications of primary detectors naturally fall into two classifications: (*a*) those used in measuring electrical quantities, and (*b*) those used in measuring nonelectrical quantities. Typical results of such classifications are shown in Tables I and II.

These tables are arranged in a form convenient for the selection of a suitable detector when the quantity to be measured together with the range over which it will vary is known, since these are the first two columns in the tabulation. In using the tables it should be understood that the "range of measurement" applies to the order of magnitude with readily available detectors and may be extended considerably for special applications using detectors specifically designed for such use. The range in many cases is shown from zero to a specified maximum and this indicates that instruments with scales indicating down to zero are available. The minimum limit of measurability in such cases depends on the sensitivity of the instrument and the allowable accuracy for the application.

The third column of Tables I and II lists a common name for the detector and this is followed by the basic elements used in the construction of the primary detector in the fourth column.

The action of the detector is described by the next three columns headed the input, the physical effect, and the output, respectively. The last column names one particular instrument in which the application of the primary detector is adequately illustrated. In most cases several other available instruments might have been used equally well to show the application of the primary detector.

A careful study of the tables together with a visualization of possible intermediate means and end devices will demonstrate the value and benefits which can be derived from such information. For instance, when it is known that accuracy requirements will dictate a servomechanical system, a primary detector suitable to this application can be selected. It is realized that the tables do not cover all the primary detectors used in measurements. It is believed that they include most of the detectors widely used in common applications. Undoubtedly additions to the tables in the future will keep them up to date with respect to new detectors which may be invented and also add to their usefulness.

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E. E. Lynch and A. J. Corson are engineers at the West Lynn (Mass.) works of the General Electric Company.

Among the authors' associates who have contributed to this study of primary detectors are I. F. Kinnard (F'43) and H. C. Dickinson (A'37). F. E. Arnold was particularly helpful in the collection of data for Tables II and III.

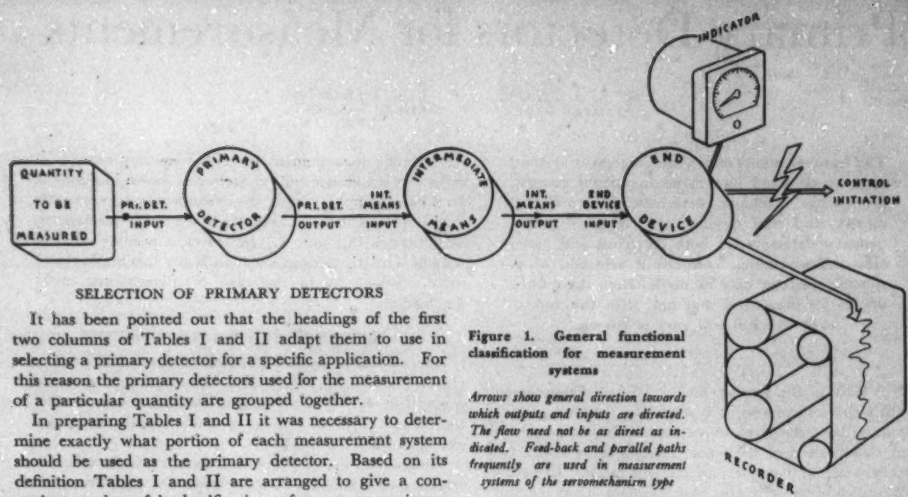


Figure 1. General functional classification for measurement systems

Arrows show general direction towards which outputs and inputs are directed. The flow need not be as direct as indicated. Feed-back and parallel paths frequently are used in measurement systems of the servomechanism type

SELECTION OF PRIMARY DETECTORS

It has been pointed out that the headings of the first two columns of Tables I and II adapt them to use in selecting a primary detector for a specific application. For this reason the primary detectors used for the measurement of a particular quantity are grouped together.

In preparing Tables I and II it was necessary to determine exactly what portion of each measurement system should be used as the primary detector. Based on its definition Tables I and II are arranged to give a convenient and useful classification of numerous primary detectors.

An example such as the permanent-magnet moving-coil mechanism (Table I) illustrates a criterion for determining which of the basic elements of a measurement sequence constitutes the primary detector. This mechanism basically detects current. The first complete measurement operation consists of the effects up to and including the production of torque. Thus, in a simple d-c ammeter the primary detector consists of the moving coil and the permanent magnet which together produce the torque. Sometimes this torque terminates in angular displacement of a spring controlled moving system. In other applications the countertorque is electrodynamic with no definite torque–deflection characteristic. For this reason the functional output of the permanent-magnet moving-coil mechanism is torque rather than deflection.

This same mechanism is also useful in measuring direct voltage. A resistance is used in series to obtain a suitable current magnitude. The operation in going from voltage to current by means of the impedance of the resistor is not considered a complete operation. This is a matter of convenience. The nature of voltage and current is so closely allied that the resistor together with the permanent-magnet moving-coil mechanism is classified as the primary detector with torque as the output. Similar combinations of resistance or impedance networks are used with electrodynamic and other detectors.

There are other examples where the permanent-magnet moving-coil mechanism is used in a measurement sequence but not as the primary detector. In the light meter (Figure 4), the nature of the cell output is so different from the input that the cell automatically falls into the classification of the primary detector.

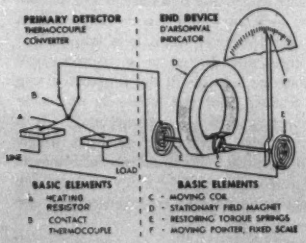


Figure 2. Radio-frequency ammeter

THE INPUT

The primary detector as shown in Figure 1 has a functional input and a functional output. These quantities are not to be confused with the power or energy input and output which are the cause and result of the primary detector's operation. For example, in Figure 3 the railway tachometer primary detector is an a-c generator with a power input and output expressable in horsepower or kilowatts. However, the functional input is speed and the functional output is voltage.

The measured quantity is often, but not always, the input to the primary detector. Cases where it is not convenient to consider the measured quantity as the input are as follows:

(*a*). When the measured quantity is represented by several component quantities and these components are used as inputs. The thermal watt demand meter, Figure 5, includes a primary detector for a-c power where alternating potential and current, phased according to their power factors, are the inputs.

(*b*). When a factor of the measured quantity is introduced elsewhere in the measurement sequence such as shown in the single-phase var-hour meter, Figure 6. Var-hours is the measured quantity, and

Table I. Typical Primary Detectors for Measurement of Electrical and Magnetic Quantities

	Quantity Measured	Range of Use	Name	Basic Elements	Input	Physical Effect Output = Kf (Input)	Output	Typical Application
1	Direct current	From 0 to 1 amp	D'Arsonval mechanism	Permanent magnet and moving coil	Direct current	The field of the magnet produces a torque on the current carrying conductors of the moving coil.	Torque	D-c ammeter
2	Direct current	From 0 to 10 amp	Moving magnet mechanism	Fixed coil moving permanent magnet	Direct current	The magnetic field of the fixed coil produces a torque on the moving magnet.	Torque	D-c ammeter
3	Direct potential	From 0 to 5,000 v	D'Arsonval mechanism	Permanent magnet, moving coil, and resistor	Direct potential	The potential, applied to a calibrated resistor, results in current $I=E/R$ which is measured as in (1).	Torque	D-c voltmeter
4	Direct potential	From 0 to 100 v	Moving magnet mechanism	Fixed coil, moving permanent magnet, and resistor	Direct potential	The potential, applied to a calibrated resistor, results in a current $I=E/R$, which is measured as in (2).	Torque	D-c voltmeter
5	Alternating or direct current	From 0 to 600 amp	Moving-iron mechanism	Fixed coil and ferromagnetic vane	Current	The magnetic field of the fixed coil induces magnetic poles in the moving vane causing attraction or repulsion.	Torque	A-c or d-c ammeter
6	Alternating or direct current	From 0 to 100 amp	Thermocouple	Resistor and thermocouple	Current	The current heats the resistor, the temperature rise of which produces electromotive force at the junction of 2 dissimilar metals.	Potential	Thermoammeter
7	Alternating or direct current	From 0 to 1.0 amp	Electrodynamic mechanism	Fixed coil and moving coil	Alternating or direct current	The magnetic fields of the fixed and moving coils interact to produce a torque.	Torque	A-c or d-c ammeter
8	Alternating or direct potential	From 0 to 750 v	Moving-iron mechanism	Fixed coil, moving iron, and resistor	Alternating or direct potential	The potential, applied to a calibrated noninductive resistor, results in a current $I=E/R$, which is measured as in (3).	Torque	A-c or d-c voltmeter
9	Alternating or direct potential	From 0 to 750 v	Electrodynamic mechanism	Fixed coil, moving coil, and resistor	Alternating or direct potential	The potential applied to a calibrated noninductive resistor, results in a current $I=E/R$ which is measured as in (7).	Torque	A-c or d-c voltmeter
10	Frequency	From 0 to 1000 c	Resonant circuit	Fixed inductors and fixed capacitors	Potential of variable frequency	The current-voltage relationship in the resonant circuit is a measure of the frequency.	Current or voltage	Frequency meter
11	A-c or d-c power	From 0 to 450 kw	Electrodynamic mechanism	Fixed coil, moving coil, and resistor	Current and potential	The magnetic fields of one fixed and one moving coil interact to produce a torque proportional to their instantaneous product.	Torque	Wattmeter
12	Power factor	From 0 to 1.00	Electrodynamic mechanism	Fixed coil, 2 moving coils, fixed resistor, and fixed inductor	Current and potential with variable power factor	The magnetic fields of one fixed and two moving coils interact to produce angular displacement when power factor is changed.	Angular displacement	Power factor meter
13	Energy (a-c)	From 0 to 10,000 kwh at rates up to 90 kw (600 v, 150 amp)	Watt-hour meter mechanism	Electromagnet, braking magnet, and eddy-current disk	Current and potential	The reaction of fluxes of the electromagnet with eddy currents induced in the disk by these fluxes produces a torque. This torque, when opposed by the braking torque of the permanent magnet, results in angular disk displacement proportional to energy.	Angular displacement	Watt-hour meter
14	Reactive energy (a-c)	From 0 to 10,000 rkva hr at rates up to 90 rkva (600 v, 150 amp)	Watt-hour meter mechanism and phase shifting transformer	Phase shifting transformer, electromagnet, braking magnet, and eddy-current disk	Current and phase-displaced potential	Same as (13)	Angular displacement	Reactive volt-ampere hour meters
15	Power demand	From 0 to 24 kw	Bimetallic actuator	Bimetallic actuators	Current	Input currents from voltage and current sources are combined in opposing bimetallic spirals to produce displacement as a function of power.	Angular displacement	Thermal watt demand meter
16	Energy (d-c)	From 0 to 10,000 kwhr at rates up to 9,000 kw (750 v, 12,000 amp)	D-c watt-hour meter mechanism	Fixed air-core coil, moving air-core coil, commutator eddy-current disk, and braking magnet	Current and potential	The magnetic fields of the fixed and moving coils interact to produce a torque. This torque when opposed by the braking torque of the magnet and disk, results in angular displacement proportional to energy.	Angular displacement	D-c watt-hour meter
17	Phase angle	From 0 to 360 deg	Phase angle meter mechanism	Electromagnetic stator, fixed coil, ferromagnetic vane, fixed resistors, and fixed capacitors	Two potentials	The interaction of magnetic fields due to two potentials causes displacement of the moving vane as a function of the phase angle.	Angular displacement	Phase angle meter (synchroscope)
18	Resistance	From 1 to 100 × 10^8 ohms	Permanent magnet moving coil mechanism	Permanent magnet, moving coil, and constant voltage source	Resistance	The constant potential applied to the resistance under measurement produces current flow $I=E/R$ which is measured as in (1).	Torque	Ohmmeter

Table I (Concluded). Typical Primary Detectors for Measurement of Electrical and Magnetic Quantities

	Quantity Measured	Range of Use	Name	Basic Elements	Input	Physical Effect Output = Kf (Input)	Output	Typical Application
19	Resistance	0.01×10^{-6} to 10×10^{6} ohms	Resistance bridge including galvanometer	Fixed resistors, variable resistors, voltage source, permanent magnet, and moving coil	Resistance	A resistance change in a balanced bridge network causes a galvanometer deflection, which results in adjustment of variable resistors to re-establish bridge balance.	Mechanical displacement	Resistance bridge
20	A-c flux	magnetic	Search coil	Search coil	A-c flux linkages	The flux linkages induce a potential in the coil. This potential is a function of the rate of change of flux.	Potential	Flux voltmeter
21	D-c flux	magnetic	Moving search coil	Search coil and means of motion	D-c flux linkages	The moving search coil is the source of a quantity of electricity proportional to the change in flux linkages.	Quantity of electricity	Ballistic fluxmeter
22	Magnetic strength	field	Moving magnet	Moving magnet	Field strength	The field exerts a torque on the magnet as a function of this field strength.	Torque	Magnetometer

time (hours) is introduced by the integrating function of the detector. The inputs are properly phased alternating potential and current and the output is the angular position of the disk.

THE OUTPUT

The functional output of the primary detector energizes or controls the operation of the end device. The output may be the input to an end device directly as shown in the radio-frequency ammeter, Figure 2, or it may be the input to an intermediate means as shown in the tachometer, Figure 3. The output of an ideal primary detector should bear an exact relation to the steady-state condition of the input. As an example, the output of a watt-hour meter mechanism (Figure 6) when measuring var-hours must be angular deflection. This is the first quantity that represents quantitative response to var-hours. The torque of the driving element is proportional to vars but not to var-hours. Torque, therefore, cannot be the output. The input–output relationship, Figure 7, must be repeatable within the required precision of measurement. This precision must be sustained in most cases over long periods of time.

If the measured quantity varies rapidly, the output inherently will contain additional transients. At times such action is desirable. A thermal watt demand meter, such as shown in Figure 5, utilizes the thermal capacity and heat insulation characteristics of the primary detector to obtain the desired transient or lagged response. In the radio-frequency ammeter, shown in Figure 2, the heat constants are so chosen that the output response is slow compared to radio-frequency cyclic changes of the input. Only the rms value of the current appears in the output. The lagging characteristics of the detector thus are utilized advantageously.

Transients also occur at times when they are undesirable. In such cases the magnitude or time constants of the transient must be negligible. If it is impossible or not feasible to accomplish this, the primary detector is unsuitable for measurement of rapidly varying quantities. Since electric networks (due to their impedance) and mechanisms (due

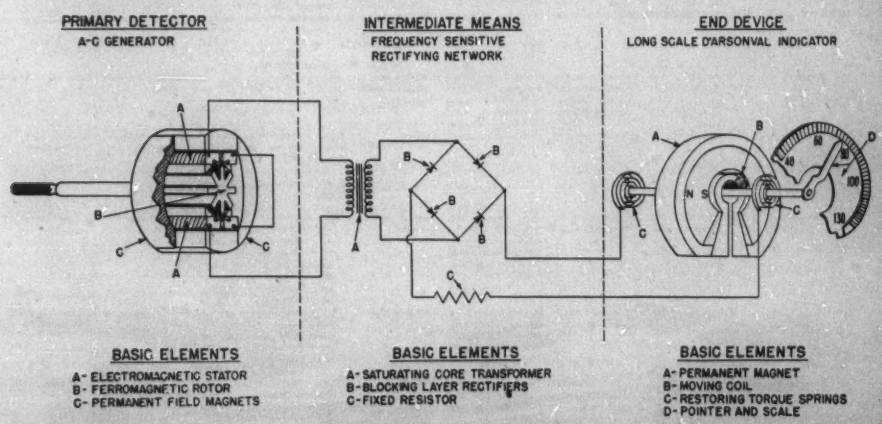


Figure 3. Railway tachometer system

Table II. Typical Primary Detectors for Measurement of Nonelectrical Quantities

	Quantity Measured	Range of Use	Name	Basic Elements	Input	Physical Effect Output = Kf (Input)	Output	Typical Application
1	Temperature	From −200 C to 1,760 C	Thermocouple	Thermocouple	Temperature	An emf exists at the junction of dissimilar metals. A temperature difference at two junctions results in a potential difference.	Potential	Thermocouple thermometer
2	Temperature	From −240 C to 600 C	Resistance temperature detector	Temperature sensitive resistor	Temperature	Electrical resistance changes with temperature.	Resistance	Resistance thermometer
3	Temperature	From 800 C to 2,000 C	Disappearing filament	Incandescent filament lamp and d-c source	Temperature	Light emitted from a glowing body depends on temperature and its intensity can be matched visually against an adjustable standard such as the filament of an incandescent lamp.	Light intensity	Disappearing filament optical pyrometer
4	Temperature		Phototube	Photo sensitive cell	Temperature	Light emitted from a glowing body depends on temperature and can be used to actuate a photosensitive cell, the resistance of which, as a circuit element, is a function of temperature.	Effective resistance	Automatic optical pyrometer
5	Temperature	From 500 C to 2,000 C	Thermopile	Radiation thermocouple	Temperature	Radiant heat from a hot body alters the temperature of a thermojunction so that the thermocouple potential difference is a function of temperature.	Potential	Radiation pyrometer
6	Temperature	From −100 C to 400 C	Bimetallic actuator	Bimetallic actuator	Temperature	Two joined metal strips with different expansion coefficients will bend as temperature changes.	Mechanical displacement	Bimetal thermometer
7	Temperature	From −190 C to 500 C	Liquid-in-glass expansion bulb	Liquid-in-glass expansion bulb	Temperature	Most materials expand with temperature increases.	Mechanical displacement	Mercury thermometer
8	Temperature	From −60 C to 550 C	Fluid-in-metal expansion bulb	Fluid-in-metal expansion bulb	Temperature	Expansion of liquid or gas due to temperature rise increases pressure in a completely filled system.	Pressure	Bourdon tube thermometer
9	Temperature	From −25 C to 325 C	Vapor - pressure bulb	Vapor - pressure bulb	Temperature	In a partially filled liquid system the vapor pressure is a function of the temperature.	Pressure	Temperature controller
10	Pressure	From 0 to 10,000 lb per sq in.	Bourdon tube	Bourdon tube	Pressure	Expansion of liquid or increase in gas pressure in a curved closed tube tends to straighten it.	Mechanical displacement	Bourdon pressure gauge
11	Pressure	From 0 to 500 lb per sq in.	Bellows or diaphragm	Bellows or diaphragm	Pressure	Increased pressure inside a flexible bellows or diaphragm tends to expand it.	Mechanical displacement	Aneroid barometer or altimeter
12	Pressure	From 0 to 20 lb per sq in.	U tube	Liquid column	Pressure	Increased head in one leg of a U tube with liquid in it displaces the liquid.	Mechanical displacement	Manometer
13	Pressure	From 10 to 2,500 lb per sq in.	Piston	Piston	Pressure	A piston with a pressure on one end will move. When balanced by pressure on the other end, the balancing force is a function of the pressure.	Force or weight	Dead weight tester
14	Pressure	From 0 to 1,500 lb per sq in.	Piezoelectric crystal	Piezoelectric crystal	Pressure	Stress causes strain in a crystal and strained crystals become electrically polarized.	Potential	Crystal-type pressure gauge
15	Vacuum	From 0.1 to 2,000 microns	Calibrated volume system	Volume and piston	Pressure	A known volume of gas at a given pressure will compress to a new measurable volume at a known pressure. $pv = c$	Mechanical displacement	McLeod gauge
16	Vacuum	From 0.1 to 750 microns	Heated resistor	Temperature sensitive resistor and standardized-voltage source	Pressure	A voltage across a resistance heats it to a temperature which is a function of the gas and pressure surrounding it. The temperature corresponds to a definite resistance.	Resistance	Pirani gauge
17	Vacuum	From 1 to 500 microns	Heated resistor	Fixed resistor and standardized-current source	Pressure	A current through a resistance heats it to a temperature which is a function of the gas and pressure around it.	Temperature	Thermocouple vacuum gauge
18	Vacuum	From 10^{-4} to 1,000 microns (cold), 0–1 micron (hot)	Electronic tube	Electronic tube	Pressure	With a controlled ionizing means, the ion collector current is a function of the pressure for a given gas.	Current	Ionization gauge
19	Vacuum	From 0.001 to 1.0 micron	Moving vane and heater	Moving vane, fixed resistor, and standardized-voltage source	Pressure	The heater creates a level of molecular energy that is higher on the adjacent side of the moving vane than on the opposite side. This difference in energy exerts a torque on the vane which is a function of the pressure.	Torque	Knudsen gauge
20	Fluid flow		Fluid motor	Fluid motor	Fluid speed	A fluid flowing through a fluid motor will move the rotor.	Angular displacement	Gasoline flowmeter

Table II (Continued). Typical Primary Detectors for Measurement of Nonelectrical Quantities

	Quantity Measured	Range of Use	Name	Basic Elements	Input	Physical Effect Output = Kf (Input)	Output	Typical Application
21	Fluid flow		Orifice	Orifice	Fluid speed	A difference in pressure on opposite sides of an orifice exists where fluid flows through.	Differential pressure	Industrial flowmeter
22	Fluid flow		Pitot tube	Pitot tube	Fluid speed	A difference between static and total head is a function of the fluid and its velocity through a pitot tube.	Differential pressure	Aircraft airspeed indicator
23	Fluid flow		Variable orifice	Float and conical tube	Fluid speed	A float in a conical tube will assume a position to vary the orifice size as a function of the fluid speed. The position of the float is therefore a function of the speed.	Linear displacement	Rotameter
24	Liquid level		Float	Float	Liquid level	A light body will be buoyant in a liquid and its position is a function of liquid level.	Mechanical displacement	Float gauge
25	Liquid level		Diaphragm	Diaphragm	Liquid level	A diaphragm is deflected by the head of liquid above and provides a pressure in a closed system which is proportional to the liquid level.	Pressure	Hydrostatic-type liquid level gauge
26	Liquid level	From 0 to 50 ft	Capacitor	Varying dielectric	Liquid level	The capacitance of a capacitor is a function of the dielectric. With two dielectrics such as a liquid and air the capacitance is a function of the dividing line position.	Capacitance	Capacitance gasoline gauge
27	Mechanical displacement		Variable capacitor	Variable capacitor	Mechanical displacement	A change in the relative position of capacitor plates alters the capacitance.	Capacitance	Capacitor-type electric gauge
28	Mechanical displacement	From 0.015 to 30 mils	Variable inductor	Variable inductor	Mechanical displacement	Small changes in air gaps of magnetic cored inductors change their inductance.	Inductance	Electrolimit gauge
29	Mechanical displacement		Voltage divider	Variable resistor	Mechanical displacement	The resistance from a fixed to a variable contact on a resistor changes as a function of the variable contact position.	Resistance	Position telemeter
30	Angular displacement	From 0 to 360 deg	D-c Selsyn transmitter	Variable resistor and d-c source	Angular displacement	A voltage impressed at opposite diameters of a tapped circular resistor sets up a voltage distribution between various taps.	Potential distribution	D-c Selsyn
31	Angular displacement	From 0 to 360 deg	A-c Selsyn transmitter	Electromagnetic stator and rotor and a-c source	Angular displacement	An excited rotor inside a polyphase stator will set up a potential distribution in the stator in accordance with the angular position of the excited rotor.	Potential distribution	A-c Selsyn
32	Angular displacement	From 0 to 360 deg	Second harmonic transmitter	Permanent magnet, saturating coil and a-c source	Angular displacement	Unidirectional flux in a polyphase coil with an a-c saturated core will set up a second harmonic potential distribution dependent on the angular position of the unidirectional flux.	Potential distribution	Second harmonic telemeter system
33	Strain	From 20 to 30,000 microin. per in.	Strain wire	Strain-sensitive resistor	Strain	Strain on wire alters dimensions and resistance of wire.	Resistance	Wire strain gauge
34	Strain	From 0.015 to 30 mils	Strain electromagnet	Variable inductor	Strain	Strain varies air-gap in variable inductor to change its inductance.	Inductance	Electromagnetic strain gauge
35	Weight or force		Spring	Spring	Force	A spring deflects as a function of the applied force.	Mechanical displacement	Spring balance
36	Acceleration	From 0 to 40 g	Spring supported mass	Solid mass and spring	Acceleration	Acceleration moves a mass with a force which is proportional to the acceleration and deflection is controlled by the spring.	Deflection	Aircraft accelerometer
37	Acceleration	From 0 to 10 g	Movable plate and electronic tube	Movable plate, electronic tube and voltage source	Acceleration	In a vacuum tube if the plate is movable, the plate current is a function of the plate position with respect to the cathode. Elastically mounting the plate makes the plate current a function of acceleration.	Current	Vacuum tube acceleration pickup
38	Speed	From 100 to 5,000 rpm	D-c generator	Electromagnetic stator, moving permanent magnet and commutator	Angular velocity	Generated potential is a function of rpm	Direct potential	Magneto tachometer
39	Speed	From 100 to 3,600 rpm	A-c single-phase generator	Electromagnetic stator and moving permanent magnets	Angular velocity	Generated potential is a function of rpm.	Alternating potential	Alternating voltage tachometer system
40	Speed	From 200 to 11,000 rpm	A-c generator	Electromagnetic stator and permanent magnet rotor	Angular velocity	Frequency of generated potential is a function of rpm.	Frequency	Aircraft tachometer or railway type tachometer system

Table II (Continued). Typical Primary Detectors for Measurement of Nonelectrical Quantities

	Quantity Measured	Range of Use	Name	Basic Elements	Physical Effect: Input	Physical Effect: Output = KF (Input)	Output	Typical Application
41	Speed	From 200 to 5,000 rpm	Magnetic - drag disk	Permanent magnet rotor and eddy-current drag disk	Angular velocity	Rotating magnet reacts with eddy currents in disk to produce a torque which is a function of rpm.	Torque	Automobile tachometer
42	Speed	From 100 to 750 rpm	Fly-ball governor	Movable weights and springs	Angular velocity	Centrifugal force on weights can be balanced against a spring force to give a displacement.	Mechanical displacement	Steam turbine governor
43	Speed		Rotating mirror	Mirror and electric lamp	Angular velocity	Light projected on a rotating mirror is reflected as pulses, the frequency of which is proportional to rpm.	Frequency of light pulses	Electronic tachometer
44	Speed	From 0 to 90,000 flashes per min	Flashing light	Discharge lamp and variable frequency source	Angular velocity	The rate of light flashes is adjusted until the rotating object appears stationary because of stroboscopic effect. When this occurs the frequency of the source is a function of rpm.	Frequency	Electric stroboscope
45	Vibration velocity	From 0.001 to 40 in. per sec	Electromagnetic pickup	Permanent magnet and moving coil	Vibration velocity and frequency	A potential proportional to the velocity is induced in a coil moving in a permanent magnet field.	Potential and frequency	Vibration velocity meter
46	Vibration acceleration	From 10 to 5,000 g	Piezoelectric pickup	Piezoelectric pickup	Vibration acceleration	A potential proportional to the acceleration appears in a piezoelectric crystal when stressed by vibration forces.	Potential and frequency	Vibration acceleration meter
47	Vibration frequency	From 15 to 500 c	Vibrating reed	Spring and mass	Vibrational frequency	When natural and forced frequencies are equal the amplitude of vibration is maximum.	Mechanical displacement	Vibrating - reed-type vibration meter
48	Sound	From 20 to 120 db	Microphone	Piezoelectric crystal	Sound	Sound energy impinging on a crystal microphone stresses the crystal and the strains produce a voltage as a function of the wave shape of the sound waves.	Potential	Sound level meter
49	Illumination	From 1 to 100 ft-c	Photovoltaic cell	Photovoltaic cell	Illumination	Radiations on a photovoltaic cell results in an emf across the terminals.	Potential	Light meter
50	Illumination	Starlight to 50,000 ft-c	Photoemissive cell	Photoemissive cell. Voltage source	Illumination	Radiations on a photoemissive cell result in the emission of electrons.	Current	Phototube light meter
51	Illumination	From 0 to 0.25 ft-c	Photoconductive cell	Photoconductive cell	Illumination	Radiations on a photoconductive cell result in a change in electrical resistance which is a function of the radiation intensity.	Resistance	Infrared meter
52	Illumination		Black body	Black body	Illumination	Light radiations are absorbed and change the temperature of a black body.	Temperature	Thermocouple bolometer
53	Brightness	From 2 to 50 ft-L	Optical wedge	Wedge filter and electric lamp	Brightness	Light of a given brightness will emerge from a wedge filter at a mechanical position dependent on the input light brightness. The position of the wedge in which the emerging brightness agrees with a standard brightness is a function of the input brightness.	Mechanical displacement	Luckiesh - Taylor brightness meter
54	Light energy		Photoemissive cell	Photoemissive cell and d-c source and capacitor	Illumination	Current from a photoemissive cell is a function of the illumination and can be integrated to give light energy for the time interval.	Current	Lumen - second meter
55	Color	400 to 700 millimicrons	Monochromatic lights and photoemissive cell	Electric lamp, prism and photoemissive cell	Transmitted light of various wave lengths	The prism disperses the light to produce the monochromatic lights which are transmitted through or reflect from the color and the resulting light forms a spectrum as a function of the color. It can be compared with a spectrum of a standard.	Spectrum	Recording spectrophotometer
56	Cosmic, X-ray and radiations		Geiger - Muller tube	Geiger-Muller tube and d-c source	Radiations	Radiations, which ionize a gas, will allow electric conduction and certain vapors in the gas will quench the current.	Current	Geiger - Muller counters
57	Humidity	From 0 to 95 per cent relative humidity	Wet and dry bulbs	Liquid expansion bulbs and moist wick	Humidity	Evaporation from the moist wick lowers its temperature below that of the dry bulb. The difference between these temperatures depends on the humidity.	Differential temperature and dry temperature	Sling psychrometer
58	Humidity	From 0 to 100 per cent relative humidity	Variable temperature chamber with reflectors	Mirror and heating and cooling source	Humidity	The dewpoint of a gas is detectable by condensation on the mirror surface as the temperature is raised and lowered.	Temperature	Recording dewpoint meter
59	Humidity	From 15 to 100 per cent relative humidity	Moisture sensitive resistor	Moisture sensitive resistor	Humidity	Certain salts become moist in humid air and their electrical resistance is a function of humidity.	Resistance	Humidity indicator

Table II (Concluded). Typical Primary Detectors for Measurement of Nonelectrical Quantities

	Quantity Measured	Range of Use	Name	Basic Elements	Input	Physical Effect Output = Kf (Input)	Output	Typical Application
60	Humidity	From 15 to 100 per cent relative humidity	Heated moisture sensitive resistor	Moisture and temperature sensitive resistor and voltage source	Humidity	A voltage applied to a salt such as LiCl which is a conductor only when moist keeps temperature such that the saturated solution vapor is in equilibrium. The temperature is then a measure of dewpoint.	Temperature	Humidity indicator
61	Humidity	From 0 to 100 per cent relative humidity	Moisture sensitive expander	Moisture sensitive expander	Humidity	Some substances change dimensions with humidity changes.	Mechanical displacement	Human hair hygrometer
62	Humidity		Capacitor	Capacitor	Humidity	The capacitance of a capacitor is a function of its dielectric and this varies with moisture content and hence humidity.	Capacitance	Moisture content indicator
63	Viscosity		Constant speed rotor	Vessel and constant speed rotor	Viscosity	The torque on a plunger in a material spinning at constant speed is a measure of viscosity.	Torque	MacMichael viscosimeter
64	Viscosity		Cup and orifice	Vessel and orifice	Viscosity	The time for a given amount of liquid to flow through an orifice is a function of its viscosity.	Time	Saybolt viscosimeter
65	Viscosity		Constant torque rotor	Vessel and constant torque rotor	Viscosity	The speed of a rotor in the material when driven by a constant torque is a measure of viscosity.	Speed	Stormer viscosimeter
66	Viscosity		Falling body	Vessel and ball	Viscosity	An object in a liquid acted on by a known force will accelerate and move through the liquid at a speed which is a function of its viscosity.	Average speed	Falling ball viscosimeter
67	Chemical composition		Diffraction grating	Defraction grating and heat source	Electromagnetic waves	A heated body radiates typical frequencies depending on its composition. A spectrum of these frequencies is formed with a grating.	Spectrum pattern	Spectrograph
68	Chemical composition	Mass range 2 to 400	Mass spectrometer	Electrodes, electromagnets, electron tube, d-c source	Mass as ionized gas	A moving charged mass will follow a given curve if acted on by a field intensity. The curve is a function of the mass.	Mechanical displacement	Mass spectrometer
69	Time	0.1 sec up	Timing motor	Timing motor and a-c standardized frequency source	Time	A constant speed motor will move through a definite angle in a given time.	Angular displacement	Time meter
70	Time	1/4 sec up	Pendulum	Lever, mass, and energy source	Time	A pendulum makes a complete oscillation in a definite time.	Number of oscillations	Pendulum clock
71	Time	0.1 sec up	Balance wheel	Mass, energy storage spring, and energy source	Time	A balance wheel mechanism makes a complete oscillation in a definite time.	Number of oscillations	Stop watch
72	Time	10^{-7} sec up	Crystal oscillator	Crystal and a-c source	Time	A crystal under controlled conditions will oscillate at a constant frequency.	Integrated frequency	Frequency standard
73	Time	10^{-4} sec up	Tuning fork	Tuning fork, electromagnet, and a-c source	Time	A tuning fork vibrates at a constant frequency under controlled conditions.	Number of oscillations	Integrated frequency control instrument
74	Time	0.01 sec up	Vibrating wire	Wire vibrator, electromagnet, and a-c source	Time	A wire under controlled conditions vibrates at a constant frequency	Number of oscillations	Load frequency control
75	Magnetic direction	From 0 to 360 deg	Compass	Permanent magnet	Magnetic direction	The flux from a horizontal permanent magnet reacts with the horizontal component of the earth's field to produce a torque to turn the magnet into the earth's magnetic field. It will therefore indicate an angle between a given direction and magnetic north.	Angular Displacement	Remote compass
76	Magnetic direction	From 0 to 360 deg	Second harmonic generator	Polyphase coil, saturable reactor, and a-c source	Magnetic direction	A distributed polyphase winding around an a-c excited saturated core will have a second harmonic potential distribution induced in it as a function of the direction of unidirectional magnetic field cutting it.	Potential distribution	Aircraft automatic pilot directional control
77	Magnetic direction	From 0 to 360 deg	Induction coil	Rotating induction coil and motor	Magnetic direction	The voltage induced in a rotating coil will be a maximum when moving to cut a maximum of the earth's magnetic field.	Potential	Induction coil compass
78	Space direction	From 0 to 360 deg	Gyroscope in universal mounting	Gyroscope rotor and energy source	Space direction	A gyroscope acted on by no torques will remain with its spin axis in a fixed direction in space. It will therefore indicate the angle between a given direction and the space direction of the spin axis of the gyroscope.	Angular displacement	Directional gyroscope or bank and climb gyroscope
79	Geographical direction	From 0 to 360 deg	Gyrocompass	Pendulous gyroscope and energy source	Geographical direction	A pendulous gyroscope with its spin axis horizontal will tend to line its spin axis with the spin axis of the earth.	Angular displacement	Gyrocompass

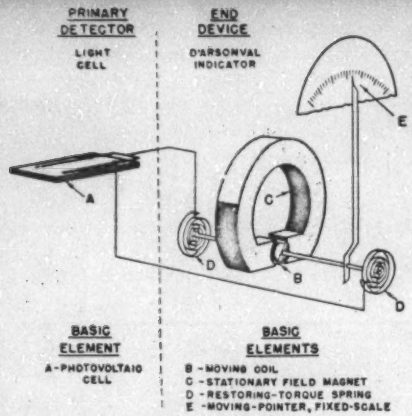


Figure 4. Light meter

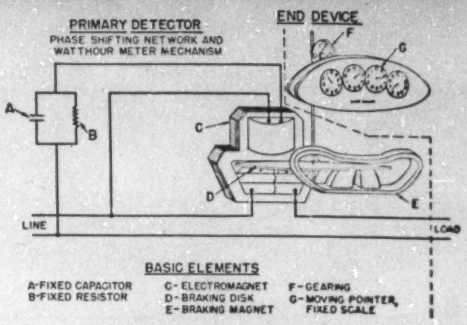


Figure 6. Single-phase var-hour meter

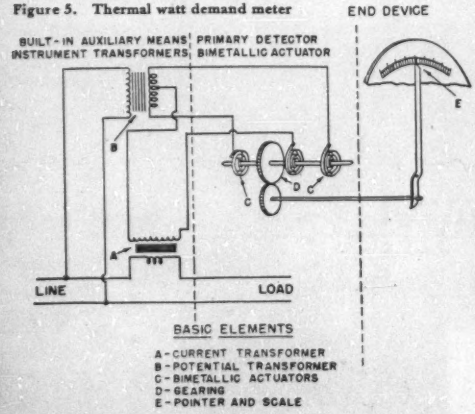
to their inertia) do introduce transients, it is necessary to analyze the transient characteristics and to know how they can be changed.

It should be understood that many quantities such as mechanical deflection, current, and potential are the inputs to primary detectors measuring that quantity, but they also may be the outputs of other primary detectors measuring entirely different quantities. For example, in Figure 4 the light cell has potential as its output. But potential can be the input to a primary detector which might be a permanent-magnet moving-coil mechanism. Mechanisms used as primary detectors in some cases may be used as intermediate means in other cases.

PHYSICAL LAWS AND EFFECTS

Nearly all physical laws have been used in measurement work, and many appear in primary detectors. Some of the important ones are listed in Table III. These laws, together with Newton's laws where mechanical motion is involved and Kirchoff's laws where electric networks are used, make it possible to set up the differential equations for the detector operation.

Figure 5. Thermal watt demand meter



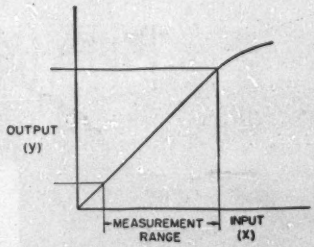
MATHEMATICAL ANALYSIS

In order to obtain an expression for the relation of the output to the input, it is necessary to understand the physical laws and effects entering into the transfer. When these laws are known the determination of the transfer characteristics becomes a mathematical one, techniques for which are covered adequately in mathematical and engineering literature.[4] Standard classical, operational, or transform methods can be used to symbolize the transfer relations. In general, a complete understanding of operation is obtained only after a thorough and sometimes complex mathematical analysis. An adequate understanding is often obtainable, however, from simple qualitative examination of the input, the output, and the physical effect involved.

In general the passive physical elements such as inductance, capacitance, resistance, inertia, and heat capacity, in the interest of simplicity, can be considered constant over the measurement interval. For the same reason, only lumped and not distributed parameters usually are assumed. Mechanical or electrical analogues may be used as well as other artifices commonly applied to the solution of

Figure 7. Input-output characteristic of primary detectors

The output (y) of an ideal primary detector is, over the measurement range, a continuous, determinate, repetitive function of the input (x) only.



differential equations. When transform methods are used to express the ratio of the output to the input, this ratio in the primary detector can be called its characteristic function. It can be used to obtain the over-all measuring system characteristic. This is the relation between the end device operation and the quantity being measured. A typical application of this method is the analysis of the watt-hour meter mechanism in the appendix.

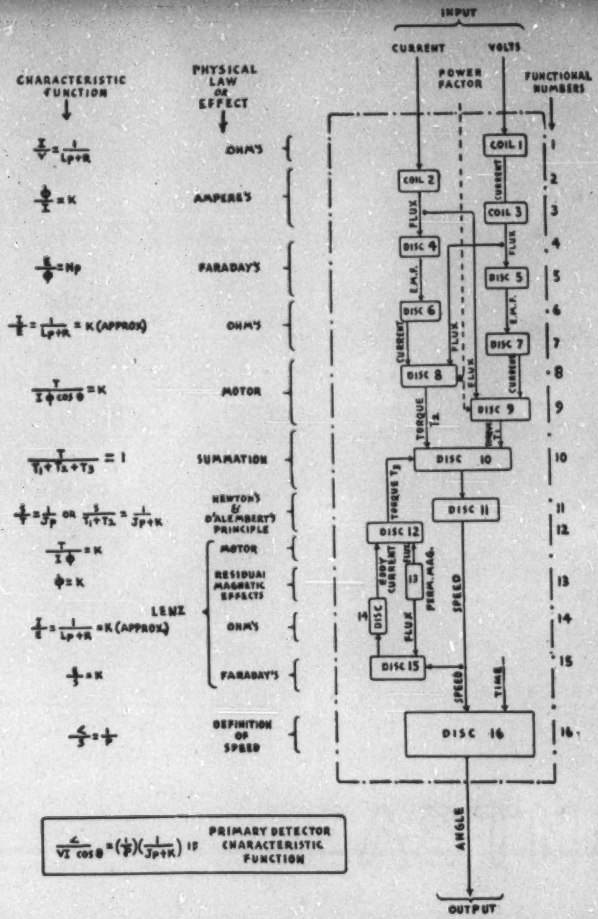


Figure 8. Detailed functional characteristics of watt-hour meter mechanism primary detector

This method not only offers a standard attack on the problem of understanding existing primary detectors, but it also provides the basis for the synthesis of the basic elements of proposed systems. Independent expressions for the transient relation and the steady-state relation between input and output can be obtained from the characteristic function of the detector.

CONCLUSIONS

The summary of this work in tabular form is intended for use in the educational field as well as in the specialized measurement engineering field. Since it is intentionally limited to a very general treatment of this phase of measurements, supplementary analysis is necessary for the complete intensive study of any particular system. A method of attack on this problem has been outlined. Finally, there is obvious need for a similar study of the remaining groups in the measurement sequence, namely, the intermediate means and the end devices, for a complete evaluation of this form of measurement analysis.

Appendix

If the detector is subdivided into its basic elements or schematically into functional blocks, a more complete understanding of results is possible. Such a breakdown is shown in Figure 8 for a watt-hour meter mechanism wherein 16 separate detail functions are carried out in three kinds of basic elements involving eight major physical effects.

A little study will show that at function number 8 and 9, torques are produced proportional to the input. Beyond this point the action is similar to a closed loop regulator since a feed-back circuit is utilized. Function 10 acts as an error detector initiating a change in response proportional to the difference of the inputs (T_1 plus T_2) and the feed-back torque (T_3). The output speed will be regulated until it is

Figure 9. Simplified diagram for determination of characteristic function of primary detector

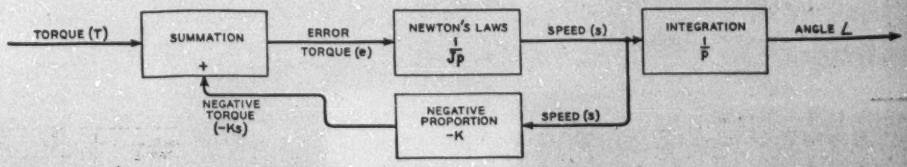


Table III. Important Physical Laws and Effects Used in Primary Detectors for Measurements

Measured Quantities	Primary Detectors (See Tables I and II)	Laws or Effects*																				
		Ampere's Law	Motor Effect	Ohm's Law	Joule's Law	Seebeck Effect	Coulomb's Law	Edison Effect	Faraday's Law	Expans./Temp	Resist./Temp	Imped./Freq.	Charles' Law	Black Body Radiation	Boyle's Law	Hooke's Law	Res./Dimension	Elastic Limit	Piezoelec. Effect	Archimedes' Principle	Perm. Mag. Phenom. Capac./Dielect.	Radiation Effect	Photoelec. Effect
Electrical																							
Potential and current	Permanent magnet moving coil	x	x	x																	x		
	Electrodynamic mechanism	x	x	x																			
	Fixed coil moving magnet	x	x	x																	x		
	Thermocouple converter			x	x	x																	
	Electrostatic mechanism			x			x																
	Electronic rectifier			x				x															
A-c power and demand	Electrodynamic mechanism	x	x	x																			
	D-c watt-hour meter mechanism	x	x	x						x											x		
	A-c watt-hour meter mechanism	x	x	x						x											x		
	Bimetallic actuator			x	x						x												
Frequency	Tuned frequency network											x											
Nonelectrical																							
Temperature	Thermocouple					x																	
	Resistance temperature detector										x												
	Bimetallic actuator										x												
	Bourdon tube												x										
	Filament comparator				x									x									
Pressure	Bourdon tube														x								
	Bellows															x							
	Wire strain gauge															x	x						
	Metal block (hardness)																	x					
	Piezoelectric crystal																		x				
Position and liquid level	Float mechanism																			x	x		
	Capacitor																					x	
	Radioactive source																						x
Light	Light cell																						x

* Dictionary of Applied Physics, Sir Richard Glazebrook, MacMillan and Company, Ltd., London, England.

proportional to the input T_1+T_2, at which time equilibrium conditions exist.

The simplified diagram in Figure 9 allows the characteristic function of the primary detector to be determined from the characteristic function of the separate blocks. If J is the disk inertia, K is the damping constant, and other terms as defined in the diagram,

$$\epsilon\left(\frac{1}{Jp}\right)=s \quad \text{.....as shown by Newton's laws in the diagram}$$

$$(T-Ks)\left(\frac{1}{Jp}\right)=s \quad \text{.....since } \epsilon=T-Ks \text{ as shown in the summation}$$

$$T-Ks=Jps \quad \text{.....by algebraically changing the position of } Jp$$

$$T=Jps+Ks=s(Jp+K) \quad \text{.by combining the terms with } s$$

$$\frac{S}{T}=\frac{1}{Jp+K} \quad \text{.....algebraically giving the speed to torque ratio}$$

$$\frac{\angle}{T}=\left(\frac{1}{Jp+K}\right)\left(\frac{1}{p}\right) \quad \text{.....since the "integration" block shows the angle equals the speed operated on by } 1/p$$

This demonstrates how the characteristic function $\angle/T$ has been obtained mathematically from the individual characteristic functions of the blocks in the diagram.

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Electrical Progress in the Textile Industry

F. D. SNYDER
ASSOCIATE AIEE

THE textile industry is probably the oldest of all industries. A study of its history and development is a study of the history and development of civilization itself. Long before electric apparatus of any kind was developed it had progressed to a stage of perfection that many of the processes employed over a hundred years ago are still in use today. Because of this high degree of mechanical development the electrical industry was for many years not pressed to contribute its share to the industry. While it is true that electric motors found early use in the textile industry, this was in the nature of a prime source of power to a purely mechanical machine. The motor was attached to a shaft on the machine and any means of using and controlling its power was done mechanically.

Within recent years electrical applications in the textile industry have made steady progress. Now, many machines and processes get their prime motive power not only from electric motors, but special motor characteristics, controls, and other electric apparatus perform many worthwhile functions. With the use of proper electric equipment, mechanically complicated processes can be improved, simplified, and done more economically. With so many different machines used in the textile industry it would be difficult to describe all the electrical details employed. There are, however, a number of basic electrical functions which apply in various combinations to many of the textile machines, and it is the purpose of this paper to describe and explain the more important. Special motor characteristics to take care of exacting machine operating conditions, controlled acceleration, controlled deceleration, various types of variable speed operation, exact tension control, exact speed control, electronic protection devices, all contribute to electrical progress in the industry. A description of actual applications shows how various results are achieved. It also becomes apparent that many combinations of electrical functions can be made to meet the requirements of other textile machines.

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F. D. Snyder is with the Westinghouse Electric Corporation, Boston, Mass.

General Purpose Applications

There are and always will be a number of applications where a motor is used only as a source of power to turn a shaft at constant speed. For such applications efficient, safe and maintenance-free electric equipment is the prime requisite. The self-cleaning squirrel motor available for this purpose is so designed that dry lint will pass readily through the motor and it seldom needs attention. For this, and other motors used in the textile industry, a permanently sealed bearing lowers bearing maintenance cost and its use eliminates the danger of a soiled product from grease or oil.

For applications where the lint is very oily or where for other reasons the self cleaning open motor cannot be used, a new screened-type motor with large screen area is available. This large screen area permits satisfactory operation even if considerable lint has accumulated on the screen, because of the extremely low-air velocity through any unit area of the screen. In some cases totally enclosed motors are necessary.

These, however, are in the nature of mechanical improvements and developments and, while they are of importance to the textile industry, they do not contribute to the actual control of a machine or process and, therefore, will not be emphasized. It is important, nevertheless, that the improved mechanical features of motors and controls for textile plant operation be utilized in building electric apparatus for all types of applications.

Controlled Acceleration

Many textile machines such as roving and spinning frames, twisters, woolen cards, and others require no special features of the motor drive other than the provision of slow, smooth acceleration to a standard squirrel cage motor. Resistance-type cushioned starters of various kinds are now being used extensively; each type designed to perform desired functions.

For cotton roving frames an automatic type is used which will cut out the resistance when approximately full speed is reached. For twisters and many kinds of winders the control is so arranged that the resistance is in the motor circuit only while the operator keeps the start button depressed. Woolen cards present a different problem. A low torque is required initially to take up any slack in the drive and then be built up to maximum in order to break static friction. After the card starts to move, the torque must be reduced again to avoid undue stress as the high pullout torque point of the motor is reached. An automatic starter for squirrel cage motors on woolen cards provides these features by inserting and removing resistance in the proper sequence.

A more satisfactory type of drive for a woolen card employs a suitably protected slip ring motor controlled by a motor-operated secondary rheostat. This gives smooth and shockless acceleration from standstill to top speed and also affords means for some variation in production speed. The same type of drive is used on wool and worsted ring spinning frames for smooth acceleration and for varying the spinning speed. The special control developed for these applications will permit the operator to preset to any desired speed and acceleration will always be at the same rate.

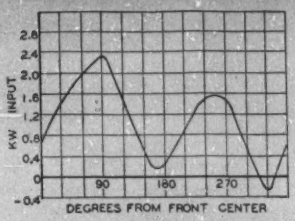

Figure 1. Instantaneous kilowatt input to a loom motor for each position of the crankshaft of a typical loom

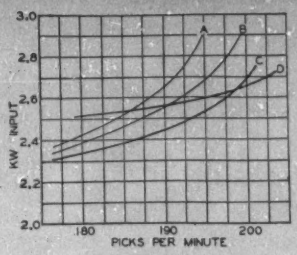

Figure 2. Curves showing peak loads of various loom motors operating the same loom at various speeds

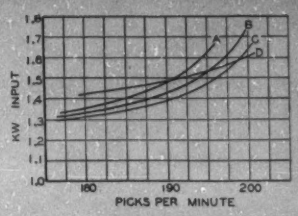

Figure 3. Curves showing average loads of various loom motors operating the same loom at various speeds

Loom Motors

With the improvement in loom design, making speeds up to 220 picks per minute practical the loom motor becomes a good example of the benefit in the way of performance and economy that can be derived from proper motor characteristics. The curve in Figure 1 shows the instantaneous power requirements for each position of the crankshaft during the operating cycle of a particular loom operating at 180 picks per minute. This shows the fluctuation of power input to the motor from 2.4 kw maximum to a 0.3 kw regenerative. These power peaks will vary as the loom speed varies but they are also affected by the design of the motor as shown in Figure 2 which indicates the maximum power input peaks to various motors all operating the same loom and the manner in which these peak loads vary as the speed of the loom is varied. Figure 3 shows the average power input to these same motors operating the same loom at different speeds.

Curve *A* in Figures 2 and 3 is for a loom motor that was perfectly satisfactory at 150 picks per minute. Curves *B*, *C*, and *D* are for loom motors all with different flywheel effect, different slip or speed torque curves and different pull-out torque. It is apparent from these curves that a substantial saving in power consumption can be made by choosing a motor with the proper characteristics and the peaks can be held well below its pull-out point.

Curve *D* is for a motor having a very high flywheel effect and comparatively low slip. Obviously, this motor should not be used for loom speeds much below 200 picks per minute. The motor represented by curve *C* has the best operating characteristics for looms running up to 190 picks per minute. It is interesting to note that all the motors represented have within one per cent of the same efficiency when carrying steady loads between 1 and 1.5 horsepower.

Warp Knitting Machines

A motor drive for a warp knitting machine should provide low torque for slow and accurate jogging during setup and adjustment periods. Further, it should provide rapid acceleration during regular starting in order to avoid starting marks together with fast and reliable deceleration to avoid stop marks.

The jogging torque is obtained by resistor-type cushioned starting. For normal starting the full voltage starting torque of the motor is generally sufficient. In some cases, however, a torque build-up is required and obtained in the same manner as explained later under drives for wide-shuttle type looms.

For stopping, solenoid or spring-operated mechanical brakes are not satisfactory because they wil not remain in adjustment and require excessive maintenance. Reversing the power to the motor in conjunction with a zero-speed switch is not satisfactory because it is impossible to decelerate to a complete stop and because the motor may reverse and damage the machine. To achieve successful rapid and positive deceleration a d-c voltage is applied to two leads of the squirrel cage motor. Figure 4 shows a family of curves for a five-horsepower 4-pole 220-volt 3-phase squirrel cage motor and indicates the decelerating torque obtainable with different values of direct current. The value of braking torque varies for different motors but the curves give an indication of what may be expected. Curves *A*, *B*, *C*, and *D* are for 10, 20, 30 and 40 amperes direct current, respectively, through two phases of the 5 horsepower motor having a full load current rating of 12.5 amperes. The full load torque of this motor is 15 pounds at 1-foot radius and from the curves it may be seen that the braking torque may go up to over eight times the full load running torque.

This type of braking is particularly desirable because the braking torque drops off very rapidly as the motor speed approaches zero.

The basic schematic diagram of the control scheme used is shown in Figure 5. For jogging, the motor is connected to the line through the adjustable resistors *R1*, *R2* and *R3* by means of contactor *A*. For running, contactor *M* provides full voltage to the motor immediately. When stopping the machine contactor *M* drops out and by the use of a suitable timing relay contactor *B* picks up momentarily and applies direct current to the motor through the rectox rectifier and the resistors *R2* and *R3*. The amount of braking can be adjusted by moving the taps on these resistors. With this scheme the motor always comes to a definite stop and it cannot reverse even though the direct current should remain on the line. The timing relay is set so that contactor *B* will drop out just after the machine has stopped.

Dressing Reel Drive

The high-speed dressing reel used in the woolen industry consists essentially of a cylinder about four feet in diameter and from 8 to 15 feet long. A sufficient number of ends or threads for a loom beam are wound onto it, section after section. The sections are built up by pulling the ends from a creel or stand on which are mounted as many individual spools of yarn as there are ends in a section. The following are the requirements of this drive:

1. The reel must start slowly until all slack yarn is taken up.
2. It must provide a variable speed of not over two-to-one.
3. When a thread breaks or a yardage counter indicates a stop, the reel must stop within one half revolution in order that a broken thread is not lost on the reel.
4. As soon as the reel has stopped it must be free to be moved by hand.

The manner in which this is accomplished is shown in Figure 6. The motor

chosen is of the slip-ring type which is well adapted to provide the two-to-one speed range and also the very slow start to allow any slack yarn to be taken up. The control is arranged so that when the operator presses the start button, contactor M will pick up but contactor A will remain open as long as the button is held down. With contactor A open the secondary resistance remains in the circuit and the motor starts slowly. As soon as all slack is taken up and the reel begins to accelerate, the operator releases the start button which picks up contactor A and allows the reel to come up to the preset speed as selected by means of the rheostat.

Stopping the reel in half a revolution requires such a high torque that it cannot be safely transmitted through the drive shaft. This is solved by the use of two solenoid brakes, one at each end of the reel. Braking torque is also provided by the motor using direct current as previously explained. As shown in Figure 6, to initiate stopping, contactors M and A are dropped out and a timing relay picks up contactor B which supplies direct current, through the rectox, to the two-brake solenoids and two phases of the motor winding, all in series. Contactor B drops out after the reel has stopped, releasing all braking action and allowing the reel to be rotated by hand. When an end breaks, stopping action is initiated by a multiple-unit electronic stop motion relay that will be discussed later.

Jack Spool Drive

An improved spooler recently developed has operating requirements similar to the dressing reel except the stopping period, from the time that an end breaks to dead stop, must be not more than two revolutions of the motor. The machine must start slowly. An operating speed range of not over two-to-one is required.

The motor chosen is again the slip ring type which provides the slow starting feature and a two-to-one speed range. The inertia of this machine is not very great and the braking torque available by applying direct current to two leads of the motor primary is found sufficient to stop the machine well within two revolutions. However, too much time is lost before braking action begins with the scheme of dropping out the line contactor and then picking up the braking contactor.

The use of the arrangement shown in Figure 7 provides satisfactory braking by saving the pickup time of a braking contactor and has the following sequence of operation. When the start button is pressed contactors M and B both pick up

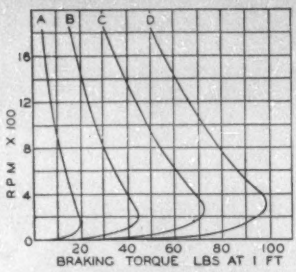

Figure 4. A family of curves showing the braking torque developed by a 5-horsepower 4-pole squirrel cage motor when direct current flows through any two legs of its 3-phase winding

and contactor A stays open. The slip-ring motor will start now with all the rheostat resistance in the motor secondary and the acceleration will be slow. When the start button is released contactor A picks up and the motor will accelerate to the speed selected by the rheostat.

The stop button, or the protecting device actuated by a broken end, will instantly drop out contactors B and A but contactor M remains closed. The full-wave rectox rectifier will supply direct current to the motor primary and start the braking action at the instant contactor B opens. A timing relay drops out contactor M and removes all power after the machine has come to rest.

Shuttle Type Looms Without Clutches

Large shuttle type looms used for the manufacture of paper mill felts, carpets, and other products have, until recently, required a friction clutch type of drive for two main reasons. First, it was the only sufficiently fast way to accelerate the loom to full speed before the shuttle is thrown across the loom the first time. Second, it was necessary in order to avoid the fly-wheel effect of the motor and gears when a protection stop is required. During this protection stop, which is required when a shuttle does not get across the shed, the loom is stopped instantly by engaging dogs, or fingers on the moving lay with the stationary breast beam. By releasing the clutch at the same instant the motor and gears are free and the dogs have to absorb only the energy from the loom itself. This, on these heavy looms, is still sufficient so that the protection stop frequently causes loom breakage. The clutch has also been desirable to allow the

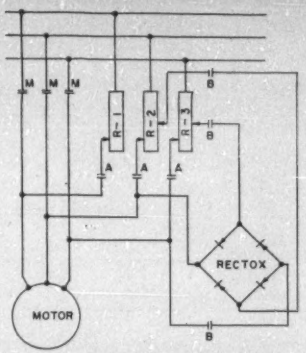

Figure 5. Schematic diagram of control for a warp knitting machine

operator to jog the loom accurately during loom fixing or set-up operations.

A drive recently developed will operate these large looms without clutches and will go through the protection stopping without shock to the loom. Variable speed is not required so a squirrel-cage type of motor is used. The basic schematic diagram of the control is shown in Figure 8, and the various functions are achieved in the following manner.

To get the required rapid acceleration a squirrel-cage motor with windings designed for Y-connection operation, is used. All the phase leads are brought out and shown as T_1, and T_4, T_2 and T_5, T_3 and T_6. When the start button is pressed contactors B, D, and M, pick up in the order named. This connects the motor winding

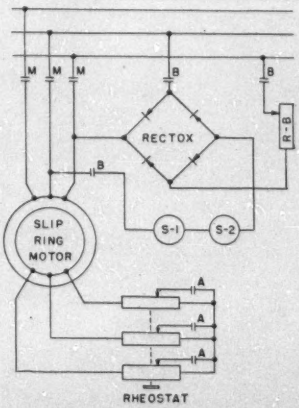

Figure 6. Schematic diagram of control for a dressing reel

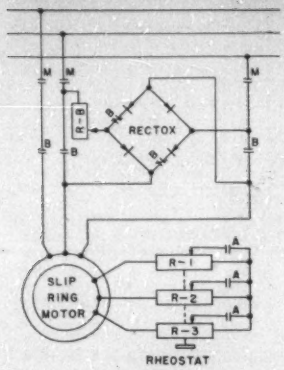

Figure 7. Schematic diagram of control for a jack spool

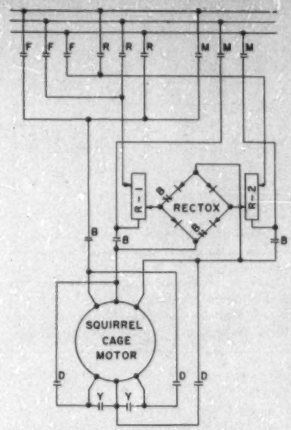

Figure 8. Schematic diagram of control for large-shuttle type loom

in delta to the line so that each phase winding is subjected to 1.73 times its normal rated voltage and a high starting torque is produced. On a particular 5-horsepower, 4-pole squirrel-cage motor tested the Y-connected torque on line voltage is 29.5 pounds at one foot for starting and 39 pounds at one foot for pullout. With the delta connection on the same motor these figures become 88.5 pounds and 117 pounds respectively. After the start button has been pressed a timing relay allows the motor to operate delta for a time long enough to permit complete acceleration, after which it drops out contactor *D* and picks up *Y*, connecting the motor winding in wye for normal operation.

When the stop button is pressed, or if the protecting switch functions because the shuttle has not arrived in the opposite shuttle box at a precise instant, contactor *B* drops out. This impresses a d-c voltage across two of the motor leads through the full-wave rectox rectifier and braking torque, as shown in Figure 4, is developed. The braking torque can be varied by adjusting the resistor *R1* and *R2*. With this scheme the loom is easily brought to rest in ample time for protection stop, smoothly and without shock. Contactors *D* and *M* drop out after the loom has stopped.

While the clutch type of drive provides only forward jogging this drive provides jogging in each direction. When the jog button is pressed contactors *Y* and *B* and either *F* or *R* pick up, depending on whether the forward or reverse jog button has been pressed. The motor is connected in its conventional, normal torque, Y-scheme and two of the motor leads are connected to the line through resistors *R1* and *R2*. By adjusting the taps on these resistors the motor can be adjusted to produce the correct amount of torque for slow and accurate jogging.

Axminster-Type Carpet Loom Drive

A complete drive for an Axminster-type carpet loom has been developed using two separate motors. The overhead-warp chain is driven by a squirrel-cage motor. Except for numerous limit switches properly interlocked to obtain the correct sequence of operation there is nothing about it worthy of consideration here. The drive which operates the loom itself has unusual features.

The Axminster loom uses a needle instead of a shuttle and should accelerate slowly compared to the shuttle-type of loom. A very slow continuous speed is required. Stopping must be fast but the decelerating torque should decrease rapidly as zero speed is approached so that the needle cable will not stretch and cause the needle to bounce backward a short distance after the loom has stopped. A variable operating speed is also required. The power requirements of an Axminster loom varies for a loom cycle somewhat in the same way as it does for an ordinary loom as shown in Figure 1. Therefore, a motor is desirable which has a drooping speed torque characteristic in order that the flywheel effect of the motor and the loom parts can furnish the peak power requirements. To accomplish this, a series d-c motor and a motor-generator set with a series-type generator is used with a special control as shown in Figure 9. It is general knowledge that a series generator feeding a series d-c motor provides an excellent variable speed drive with good regulation. It has many inherent qualities which, by proper control, are advantageous in the operation of an Axminster loom.

Contactor *M* connects the a-c motor of the motor-generator set to the a-c supply and this contactor remains closed at all times except during complete shutdown such as at the end of a working period. With the motor-generator running and the loom shut down, contactors *C* and *B* are picked up. This short-circuits the generator series field and sets up a circuit from the generator armature through the motor series field and resistor *R2*. The generator field, however, has such a low resistance that some current will flow through it and resistor *R1* is used to limit the current and thereby prevent the generator from becoming self-exciting with the *R2* resistance load, and yet provide sufficient field flux to aid the residual voltage so the motor series field will have roughly 30 per cent of the motor full load current flowing through it

When the jog button is pressed contactor *A* will pick up and impress a voltage across the motor armature equal to the drop across resistor *R2* causing it to rotate instantly since its field is already energized and the build-up time of the field has been saved. When the jog button is released, contactor *A* drops out and the motor stops by dynamic braking.

To obtain a continuous slow speed, con-

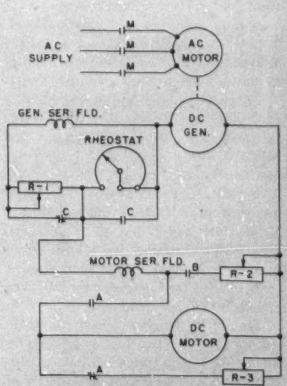

Figure 9. Schematic diagram of control for the main drive of an Axminster loom

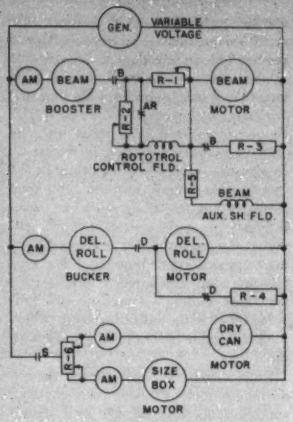

Figure 10. Schematic diagram of variable voltage armature circuits for a slasher drive

tactor *A* picks up and then *B* drops out. This provides a slightly higher speed than the jogging speed since resistor *R2* is no longer in the circuit and the exact speed is determined by the setting of resistor *R1*. Since the slow speed does not have to be exact, the setting of resistor *R1* required to obtain the correct current through the motor field at standstill, will be satisfactory.

For high-speed operation contactor *A* picks up and then contactors *C* and *B* drop out in that order. The current from the generator will now divide between the generator series field and the speed-control rheostat. Due to the time constant, the voltage build-up on the generator will be slow and the motor will, therefore, build up its torque slowly and accelerate slowly. The actual speed of operation is determined by the setting of the speed-control rheostat. During the high-speed operation the sudden peak loads develop more rapidly than the generator voltage can build up to compensate for the increased load so that the motor slows down and the flywheel effect provides the peak. During this interval, the generator voltage is building up, when the load peak has passed, the motor will be restored quickly to its original speed.

When the stop button is pressed contactor *B* closes at the same instant contactor *A* opens and contactor *C* closes immediately after *A* has opened. Closing contactor *B* places a high voltage across the motor series field and resistor *R2*. The motor field builds up rapidly and since the motor armature is short-circuited through resistor *R3* strong dynamic braking results. Excessive current through the motor series field and generator armature is avoided since contactor *C* picked up and short-circuited the generator field, at which time the generator voltage immediately started to drop. However, long before the current through the motor field has dropped down to its permanent standstill value dynamic braking will have stopped the loom motor.

Slasher Drive

Slashers come under the general category of textile range drives. A range drive may be described as a drive for a process where two or more sections or machines are driven by individual motors controlled so that the various sections will handle a continuous flow of cloth or yarn and provide, at all times, the tension or stretch desired between sections. Because most range drives require a wide range in speed the variable voltage d-c type of drive now is almost universally used. Into this scheme can be incorporated such devices as the Rototrol or rotating regulator, tachometer generators and others. Electronic conversion from alternating to direct current with its inherent characteristic of accurate controls is used also at times instead of motor-generator set. Range drives have improved rapidly during the past few years, but probably the greatest advances have been made in the slasher drive which requires an extremely accurate control.

A slasher is a machine which takes yarn from a series of spools or beams, combines the ends into a single sheet of yarn, runs the sheet through a starch or sizing bath, then through or over a drying medium and finally winds the yarn onto a loom beam. For the purpose of describing such a drive we have chosen a slasher with four motors driving the size box rolls, the dry cans, the front delivery rolls and the beam or take-up spool. In order to meet all the requirements a d-c variable voltage drive with a number of unusual features, is used. These will be described hereafter and the basic diagram is shown in Figures 10, 11 and 12.

A range of speed is required in the order to 2 to 5 yards per minute for creeping and of from 10 to 90 yards per minute for production. This wide speed range is obtained by a variable armature voltage on all the motors. Figure 10 shows the motor armatures connected to the variable voltage generator and whenever a different operating speed is required the voltage to all the motor armatures is raised or lowered. The variable voltage generator is equipped with a main shunt field which is used to control the voltage over the production speed range and an auxiliary shunt field which is used to control the voltage at stalled tension and for creeping speeds under the several conditions as will be explained later.

The wind-up beam must be core-driven

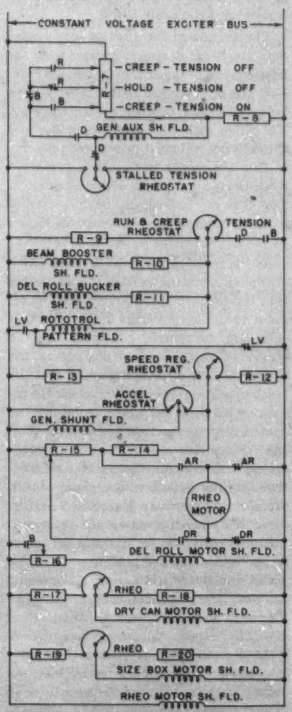

Figure 11. Schematic diagram of field excitation and control circuit for a slasher drive

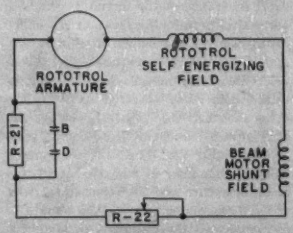

Figure 12. Schematic diagram of Rototrol or tension regulating circuit of the beam motor for slasher drive

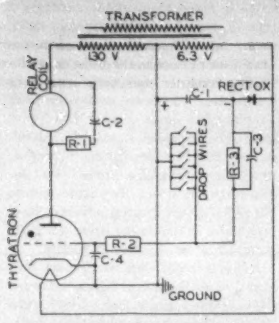

Figure 13. Schematic diagram of a single-unit type of electronic stop motion relay

by the beam motor. Therefore, for any given production speed the motor speed must be variable over a 6-to-1 range to compensate for build-up from empty to full speed and simultaneously hold a constant yarn tension over this range. To maintain a constant tension at a particular yarn speed it is only necessary that the beam motor develop a constant horsepower in going from the empty to the full beam speed. At a given yarn speed the armature voltage remains constant, consequently, if the current of the beam motor is kept constant, it will automatically develop a constant horsepower and, therefore, wind up the yarn at a constant tension. This is accomplished by using a Rototrol or rotating regulator to supply the field current for the beam motor as it operates through its 6-to-1 speed range by field control. Figure 12 shows the schematic diagram of the Rototrol circuit. The Rototrol armature is driven by the a-c motor that drives the variable voltage generator and the constant voltage exciter. In production contactors *D* and *B* are closed short-circuiting out resistor *R21*. Resistor *R22* is adjusted so the total resistance of the circuit becomes a value that permits the series or self-energizing field of the Rototrol to maintain any desired current through the shunt field of the beam motor once that value is established. The Rototrol has two other fields shown as the Rototrol control field in Figure 10 and the Rototrol pattern field in Figure 11. The Rototrol control field is a measure of the current in the beam armature and is connected to help the self-energizing field. Contactor *LV* in Figure 11 is always picked up when the slasher is in operation and the Rototrol pattern field is set up to oppose the control field. Assume that the run and creep tension rheostat in Figure 11 is set to a point where the amount of current that will flow through the pattern field will require ten amperes through the beam armature in order for the control field to balance exactly the pattern field. Then, if for some reason, there are not exactly ten amperes flowing through the beam armature, the control field will help or the pattern field will buck the self-energizing field until the beam shunt field is of the correct value to make the motor operate at ten amperes. The control and pattern fields are now neutralized and the self-energizing field maintains the proper beam motor shunt field current. As the yarn is wound onto the beam and the diameter increases, the beam motor current will go slightly over ten amperes. The control field will, therefore, overbalance the pattern field and aid the Rototrol series field causing its voltage to rise and strengthen the beam motor field. The speed of the motor will tend to reduce until the ten amperes are again obtained. This regulation continues until the beam is full.

The tension of the yarn as it is wound onto the beam is always greater than the friction load of the entire slasher. Therefore, the delivery roll motor must always hold back and operate as a generator. The drive ratio between this motor and the delivery roll must be selected properly so that the motor can act as a generator while the beam motor is acting as a motor. The delivery roll shunt field is excited from the exciter bus as shown in Figure 11.

As the speed of the drive is varied by a change in armature voltage it is necessary that the torques of the several motors remain at the same values if the yarn tensions are to remain constant. At the higher voltages this is true, but as the lower voltages of creeping speed and the lower operating speeds are reached, the resistance drops of the motor armatures become a large percentage of the total voltage. Since this resistance drop percentage is different for the several motors, their torque relationships change and, therefore, the tensions change. To overcome this, where necessary, a booster generator is placed in series with the beam motor and a bucker generator in series with the delivery roll motor. The shunt fields of these two generators are shown in Figure 11 and they are adjusted automatically as the run and creep tension rheostat is adjusted, so that both generators will produce the correct voltage to overcome the armature resistance drop of their respective motors at all tension settings.

The dry can and size box motors usually operate at low load and, therefore, the armature resistance drop does not have as pronounced an effect as it does on the other motors. The correct tension can be maintained usually between size box and dry can, and dry can and delivery roll by the use of resistor *R6* in series with the motor armatures, as shown in Figure 10, to give the motors a slight droop in the speed-torque curve and by providing rheostats for their shunt fields, as shown in Figure 11, so the operation of the individual motors can be changed from motoring to generating as required. For very exacting conditions a booster may be used for these motors. Ammeters are provided for all of the motor armatures which serve as a guide to the operator for setting up.

Besides maintaining constant tension during production speeds, other exacting conditions have to be met in a slasher drive and these are accomplished as follows.

Stalled Tension. If for any reason the slasher is stopped with yarn on the beam there must remain sufficient tension on the beam motor to hold the yarn tight but not sufficient so that the machine will creep. When stalled tension is desired contactors *D* and *S* are open but *B* remains picked up. With these conditions from Figure 10 the beam motor is connected to the generator but the other motors are open. The accelerating rheostat, Figure 11, has been moved by the rheostat motor to the position of the solid arrow at the left and the main generator shunt field voltage becomes zero. With contactor *D* out, the generator auxiliary field is energized through the stalled tension rheostat. Figure 11, and is connected so that the auxiliary field may vary from positive to negative which may be required to partially overcome residual voltage of the variable voltage generator.

With contactor *B* open it can be seen from Figure 11 that the beam booster shunt field, the delivery roll bucker shunt field and the Rototrol pattern field are open. The Rototrol circuit, Figure 12, has resistor *R21* inserted in the line which detunes the circuit so that it is no longer self-energizing and, therefore, will not attempt to regulate for constant current. The beam motor produces a torque depending on the setting of the stalled tension rheostat. Changing this rheostat to obtain a higher generator voltage will cause more current to flow through the beam motor armature. This in turn causes more positive excitation for the Rototrol from its control field and a stronger beam motor field, and thus, the torque of the beam motor increases both

because of a change in armature voltage and field strength.

Hold With Tension Off. When a full beam is doffed the beam tension must be removed. At this time the delivery roll motor must develop torque to keep the yarn under tension from the delivery roll back through the rest of the machine. To do this contactor *B* drops out and *D* picks up and the delivery roll motor is energized. The correct armature voltage to hold the slasher is provided by adjusting the hold tension-off connection on resistor *R7*, Figure 11, which adjusts the voltage on the generator auxiliary field.

Creep With Tension Off. At times, it is necessary to move yarn through the slasher with the beam motor off. This requires a slightly higher voltage than the preceding hold position and this is accomplished by picking up contactor *R*, Figure 11, which adjusts the generator auxiliary shunt field through the creep tension-off point of resistor *R7*. At the same time contactor *S* picks up so the size box and dry-can motors can develop torque, and with contactor *B* open the delivery roll motor shunt field is weakened so it will tend to stay ahead of the dry can and size-box motors and maintain tension.

Creep With Tension On. With the warp in place on the beam and under tension, pressing the creep button will put all the motors on the line. The main generator shunt field remains at zero voltage and the auxiliary field provides excitation for the necessary armature voltage by the proper setting of the creep tension-on point of resistor *R7*. At this time the Rototrol is functioning and the beam motor and delivery roll motor have their boosting and bucking generators respectively connected into the circuit. Tensions are, therefore, maintained accurately at the same value as when the slasher is running at production speed.

Acceleration. Acceleration from zero or creeping speed to operating speed is accomplished by the motor operated accelerating rheostat, Figure 11. During this period the accelerating rheostat moves from the solid arrow position at the left, Figure 11, to the dotted arrow position at the extreme right. The main generator shunt field builds up from zero to the value set by the speed regulating rheostat. During this time contactor *AR* picks up which operates the rheostat motor, Figure 11. It also inserts resistance *R2* into the Rototrol control field which has the effect of requiring more current through the beam armature to balance the setting of the Rototrol pattern field. The slasher accelerates with the beam motor being controlled by the Rototrol to produce a higher torque than its pattern field setting so that part of the torque is available for acceleration and part to maintain the required tension.

In order to get about the same rate of acceleration regardless of the setting of the rheostat motor is automatically decreased as the speed regulating rheostat is set for higher speeds. Figure 11 shows the manner in which the armature voltage of the rheostat motor is caused to vary inversely as the voltage setting of the generator main field as it is determined by the creep and run tension rheostat.

Deceleration. When it is desired to decelerate to either creep or stop, the power remains on all motors while the rheostat motor moves the accelerating rheostat back to zero voltage position. If deceleration is from run to creep all motors remain on the line and continue to operate at the creeping voltage. If deceleration is from run to stop contactors *D* and *S* drop out and disconnect the delivery roll, dry-can and size-box motors, but enough voltage is left on the beam motor to maintain stalled tension. The speed of the rheostat motor during deceleration is determined by the adjustment of resistor *R15*, Figure 11.

Note that positive stopping of the rheostat motor is provided by dynamic braking in both the accelerating and decelerating directions.

Emergency Stop. An emergency quick stop is available by dropping out contactors *B*, *D*, *S*, and *LV*, Figures 10 and 11. This provides strong dynamic braking of the beam and delivery roll motors and a full field is insured for the beam motor since the Rototrol pattern field is reversed. This stop is for emergency only and will not maintain yarn tension.

Electronic Devices

The textile industry is alert to the possibilities presented by the use of electronic devices. Silk and synthetic yarn mills clean air by electrostatic means before it is circulated through dryers. Electrostatic means are used also to clean all the air in many weave sheds and spinning and winding rooms. Dielectric heating is used for twist setting of various yarns and to dry yarns under certain conditions. Ultraviolet generators are used to aid chemical reaction of some finishing processes. Electronically operated and controlled d-c motors are used for many applications.

Electronic relays are being used for the safe, fast and positive stopping of machines when single threads break or where bobbins in loom shuttles run out. Figure 13 shows a single-unit type of electronic relay. The drop wires, which are small open metallic contacts, are held up by individual threads and while all the drop wires are held up the grid of the thyratron is held negative so the tube cannot fire. As soon as a thread breaks and its drop wire falls, the grid becomes positive and the tube fires and picks up the relay. This action is dependable even though the drop wire contact is made only for a few micro seconds and the contact resistance, due to dirt and corrosion, is as high as 30,000 ohms. The voltage across the open drop wire is about 10 volts with one side grounded, so open contacts are not dangerous. At the time the drop wire contact is made the current is so small that there is no arcing or burning on the lightest of drop wire or contact making schemes.

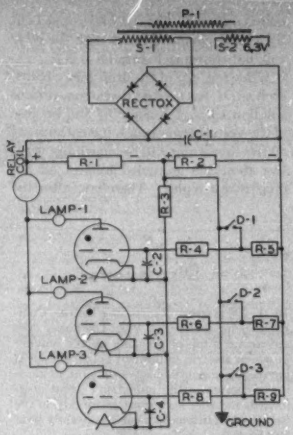

Figure 14. Schematic diagram of a multiple-unit type of electronic stop motion relay

A similar electronic relay is shown in Figure 14, except this is a multiple-circuit type. As many groups of drop wires as are desired are connected as shown, one group to *D1*, one to *D2*, and so on, using as many thyratrons as there are drop wire, or indicating groups. If a contact is made at *D1* its thyratron will fire and pick up the relay and also illuminate the indicating *Lamp-1*. Since the thyratrons are operated by direct current once they are fired they will remain conductive and keep the relay picked up and the lamp illuminated even though its drop wire cir-

cuit is opened. The relay is dropped out by a reset circuit in series with the relay coil, not shown in Figure 14.

When any one of the drop wire circuits, such as *D1*, has fired its thyratron, current will flow through resistor *R3* and because of the voltage drop in *R3*, the cathodes of all the thyratrons will become more negative than the grounded lead of all the drop wire circuits. Therefore, after the relay has picked up and stopped the machine, should any or all of the remaining drop wires close, they cannot cause their respective thyratrons to fire and only the lamp corresponding to the drop wire that first made contact will be lighted and the operator will know exactly where the trouble originated.

Electrical progress in the textile industry has already contributed to the production of better textile products and to the production of these products at costs so they can come within the reach of more and more people. The close co-operation between the textile mills, the textile machinery builders and the electrical manufacturers will continue to produce results to the end that more and better textiles and textile products will be available to all people everywhere.

Discussion

R. J. Demartini (General Electric Company, Schenectady, N. Y.): The paper does an excellent job of pointing up the manner in which electric equipment is being used in increasing measure to solve difficult processing problems in the industry. What comment is in order here must be largely in the nature of additions to what has already been said so well.

Loom Motors

From the curves of kilowatt input versus picks per minute (Figures 2 and 3 of the paper) it is apparent that by proper combination of motor inertia and electrical characteristics, a loom motor may be designed for optimum performance for a specific application. To this should be added the statement that, however good the electrical characteristics of a motor driving the pulsating loom load may be, addition of inertia results in gains in three directions.

1. Losses are less, hence power consumption is less and in some cases it becomes possible to use smaller motors.

2. Speed variation over the operating cycle is less, hence what the fixer refers to as the "feel" of the loom is improved.

3. Average power factor is better.

It is for this reason that rotors of most loom motors have greater inertia than do those of general purpose machines. Economics, however, limit the designer in the extent to which he can go in securing Wk^2 by way of larger rotors. For these reasons, much work has been done on the development of loom motors with flywheels added. See Figure 1 of the discussion.

This is not to say, however, that an external flywheel should be added to every loom drive. Rather, each application should be considered individually and the cost of such an addition should be balanced against the value of benefits to be gained.

Warp Knitting Machines

Operating advantages realized from electric braking of warp knitting machines have won general acclamation in the industry. Although there are theoretical advantages to be gained from the use of d-c dynamic braking as opposed to plugging, in the several installations of plugging control that method has proved to be highly satisfactory. Today's plugging switch design incorporating the "alnico" permanent magnet mechanism is a very reliable device. The rate of deceleration by this method is equally as good as that obtainable by d-c dynamic braking in this application. Considerably fewer devices are required, and as a consequence the cost of control is only about one-third of that of the competitive method. Maintenance too is reduced.

Axminster Carpet Loom Drives

Justification for the use of an adjustable speed drive on an Axminster carpet loom lies largely in the fact that mechanical adjustments on this machine are critical and must be made at various reduced speed levels. Mills also appreciate having a means for increasing loom speed in small increments as machine cams and other elements are gradually improved. Having proper operating characteristics during the normal run part of the cycle as pointed out in the paper, is of great importance to the mill. However, recognition must be accorded the fact that mills engaged in Axminster carpet weaving differ in the manner in which they expect the drives to operate. It is, therefore, important that there be incorporated in the design a degree of flexibility which will insure that the electrical equipment will be capable of satisfying demands of the various schools of thought on Axminster loom operation.

Consideration of the features incorporated in such electronic motor controls as the *Thymo-trol* reveals that these offer certain advantages not present in the motor-generator set drive as described. These are:

1. The rate of acceleration is adjustable. In this regard the requirements of two plants of a single company have been found to differ considerably.

2. The amount of droop in the speed-torque characteristic of the drive, though fixed by machine characteristics in the motor-generator set equipment, in the electronic drive is adjustable by setting of the *IR* drop compensation potentiometer.

3. With the field heating, there is an inevitable speed change in the drive described (as much as several picks per minute). A field current regulator, which is part of the electronic drive prevents this.

4. The design of the series generator drive requires that the generator be located close to the loom drive motor, in the lint laden atmosphere. Unless a totally enclosed set is employed, involving large, costly derated machines, there is introduced a fire hazard in the weave shed. The electronic rectifier has no commutator, and hence this problem is nonexistent.

Electronic Devices

Features of electronic control have been perhaps over-glamorized in its relatively few years in industry. Many mill maintenance people are as yet unfamiliar with such equipment. For these reasons good electrical application engineering has concentrated its efforts in those fields where a job can best be done or can be done only with the use of electronic control. Such criteria are hardly restrictive, however, for there have proved to be a great many places in the textile industry where the use of tubes is warranted. Seam detection at high cloth speed, liquid level indication without the use of floats, co-ordination of the speeds of succeeding motors in finishing ranges without the necessity for tension in the fabric between, photoelectric weft straightening, and hosiery machine drives are all examples of such equipments. The hosiery machine application is an example in which electronic drives have proved so successful that in the short time since the end of the war over 1,000 have been put in service and some 1,000 more are on the way.

Today's labor costs, after the tremendous increases the textile industry has experienced relative to other industries over the past eight years, justify expenditures for automatic electrical equipment. The opportunities for mills, machinery manufacturers, and the electrical industry to co-operate in the achievement of the ideal of more goods

Figure 1. Loom motor with built-in flywheel

for more people at lower cost in textiles are limitless.

F. D. Snyder: If a loom motor is designed for optimum performance by the proper combination of motor inertia and electrical characteristics for a specific application, then the addition of inertia by the use of a flywheel cannot result in gains. If a flywheel does result in gains then it can only mean that the motor was not designed for optimum performance. It is perfectly possible that one specific application needs more flywheel effect than another but if such is the case, in order to get optimum performance, the electrical characteristics must be changed at the same time.

Warp Knitting Machines

The fact remains that even with reliability of the present day "alnico" permanent-magnet type of plugging switch and the reliability of present day contactors, warp knitting machines equipped with such devices do occasionally reverse. It requires only one reversal to pay for many d-c braking controls.

Furthermore, d-c braking with line voltage and the proper rectifier produces a greater rate of deceleration than plugging at line voltage.

Axminster Carpet Loom Drives

The series type d-c variable speed drive as described in the paper is particularly well adapted to meet all the Axminster loom requirements. The various points brought out in the discussion can be answered by the following remarks.

1. The rate of acceleration can be adjusted in series variable type of drive by means of a simple reactor. However, this is not necessary if the drive is designed to accelerate smoothly and slowly in the first place.

2. The series variable drive has practically no droop in its speed torque characteristics. It does inherently drop the motor speed as load is applied but it slowly comes back to its original speed. This provides an excellent characteristic for the shockless acceleration of the needle motion. The characteristic is again adjustable by the use of a reactor.

3. The series fields of both the motor and the generator in this system is so low that the change in resistance value due to temperature change is very little. However, since both of these fields are in series with the motor armature and since most of the generated voltage is accounted for by motor counter electromotive force even a large change in resistance of the fields would mean little change in motor speed.

At the lower speeds where the generator field has a shunting resistor there might be a few per cent speed if the resistors are not designed properly but at the higher operating speeds the cold and hot speeds are very nearly the same.

4. The electronic drive still requires the same protection for the motor. Actually, a large number of properly screened d-c motors are being successfully operated in many carpet mills.

Electronic Devices

Demartini and the writer agree that the textile industry has many uses for electronic devices of all kinds. All that is necessary for electronic apparatus to be adopted for any given application is for this apparatus to prove that it is the most economical means to an end. Many applications have already proved economical, others will follow continuously as further research in their use continues.

Trends and Practices in Modern Industrial Power Distribution Systems

W. C. BLOOMQUIST
MEMBER AIEE

THE main purpose of this paper is to review present practices and trends of industrial power-distribution systems and to indicate, even though briefly in some cases, how they may be applied to new plants or in modernization and expansion of existing plants.

Many of our present tools were introduced in connection with the war effort when speed, ease of installation, reliability, and ease of operation and safety to personnel were so important. Although cost was often of secondary importance in the war construction program, yet because of the introduction of sound engineering principles of power distribution and the use of standardized factory-assembled equipments the over-all cost of the electrical power distribution system was low.

The main objectives for a power-distribution system are reliability, safety, low cost, flexibility, ease of operation and maintenance, and ease of installation. This paper shows how these features can be incorporated in a power system.

Present day practices and trends of the most important factors influencing a power system include load-center system of power distribution, higher main distribution voltages (4,160 or 13,800 volts) grounded system operation, protective devices of adequate interrupting capacity, and the use of metal-enclosed unit-type equipment.

Selection of Proper Voltage

System voltages can generally be conveniently divided into the following classes:

1. Utilization, generally below 600 volts.
2. Main distribution or generation, usually 2.4 to 13.8 kv.

In discussing the selection of system voltage, it will be well to first differentiate between the voltage rating of utilization equipment and generation or distribution equipment. The nominal system voltage corresponding to these is given in Table I and whenever the system voltage level is referred to, it is the nominal system voltage that is implied.

The selection of system voltages should be on the basis of availability of equipment and over-all economic considerations which are influenced by the plant electrical capacity and the cost of component parts of the power system including switching equipment, distribution channels, and transformers.

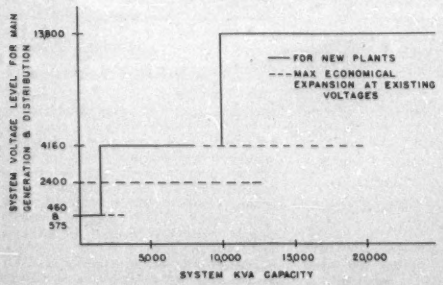

Figure 1. Selection of main distribution or generation voltage in terms of plant electrical capacity

Use of 460-Volt System for Low-Voltage Utilization Equipment

Although a 575-volt system is slightly more economical than a 460-volt system, the latter is by far the most common, due to the greater availability of utilization equipment. There are very few new 575- or 230-volt systems except as an extension of an existing system and then mainly in the interest of interchangeability of utilization equipment.

4,160-Volt Versus 13,800-Volt Systems

There is a definite trend towards the selection of 4,160 or 13,800 volts for main plant distribution. The use of 2,400 volts for main power distribution is decreasing, primarily because of cost and engineering limitations associated with expansion. Figure 1 is a guide to the selection of the main plant distribution or generation voltage for new plants and shows also the extent to which existing plants can be reasonably expanded.

Based on the installed cost of all components comprising the distribution system, a 4,160-volt system is generally more economical than a 13,800-volt system for plants up to about 10,000 kva. For plants in the range of 10,000 to 20,000-kva capacity, the two are comparable and above 20,000-kva capacity, a 13,800-volt system is usually the logical selection.

If past experience is any guide to future expansion, it would be well to allow for ample electrical growth in selecting the

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W. C. Bloomquist is in the industrial engineering department, General Electric Company, Schenectady, N. Y.

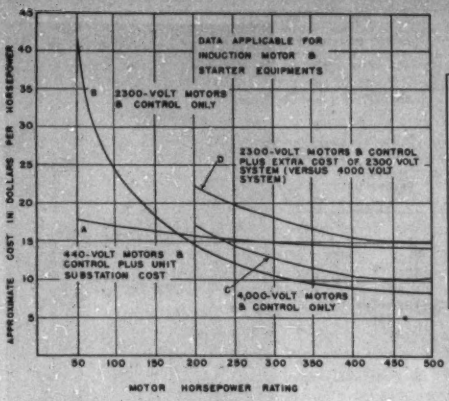


Figure 2 (left). Cost of motors, control, and power system versus motor horsepower rating

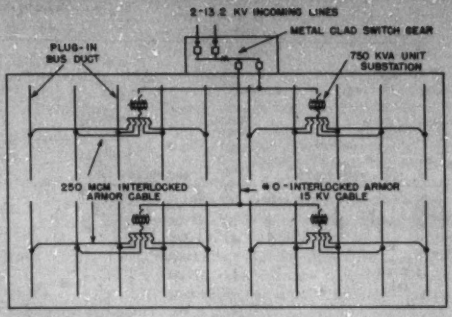


Figure 4. Load-center method of power distribution

main voltage level and system arrangement.

440-Volt Versus 2,300- and 4,000-Volt Motors

Many plants use a large number of induction and synchronous motors in the 50 to 200 horsepower class. Since motors of this rating are available at 2,300 volts, it leads to the desire to select as many motors as possible for operation directly from a 2,400-volt main distribution system on the assumption that such an arrangement results in the lowest over-all system cost. Also, since 2,300-volt motors cost less than 4,000-volt motors, it has been assumed that a 2,400-volt distribution system costs less than a 4,160-volt system.

An examination of many cases showed that these assumptions are not true.

The results of these studies are included in Figure 2 and show that motors rated 200 horsepower or less should be operated on a 460-volt system and that a 4,160-volt power distribution system is more economical than a 2,400-volt system where large motors are operated directly at the main distribution voltage. Although these data pertain specifically to induction motors, they are also generally true for synchronous motors.

Where a 2,400-Volt System Is Applicable

A 2,400-volt system is applicable where there are large and concentrated motor loads served by step-down transformers from a 13.8-kv main distribution system. In this case, a 2,400-volt system rather than a 4,160-volt system is preferred. This choice is based on the relative costs of motors and control only and, as was shown previously, 2,300-volt equipment costs less than 4,000-volt equipment. Here the cost of the distribution system is not a factor, since the cost of transformation to 2,400 or 4,160 volts is practically the same.

Load Center System of Power Distribution

Prior to about 1941, there had been no major advance in power-distribution systems for industrial plants except for the introduction of metalclad switchgear and Askarel-filled transformers. Power systems were individually studied, and in most cases without proper evaluation of all components, and there was very little attempt to study basic patterns.

About 1941 a new approach was taken in studying industrial power systems, with particular reference to manufacturing

Figure 3. Old method of power distribution

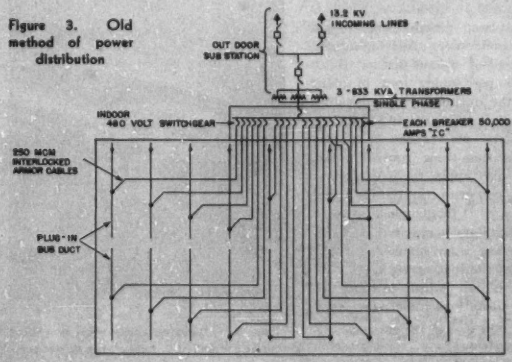
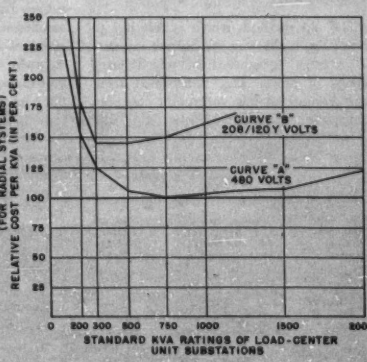


Figure 5. Relative costs of load-center power distribution system in terms of unit substation rating



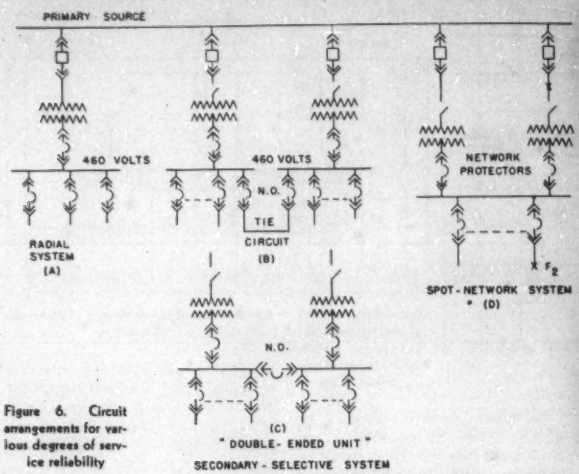


Figure 6. Circuit arrangements for various degrees of service reliability

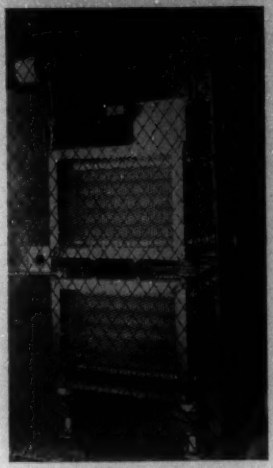


Figure 7. Indoor cast-grid type grounding resistor connected to generator neutral

plants, and resulted in the load-center principle of distribution, which is now the accepted method of power distribution. Although the load-center principle applies to power distribution at all voltage classes, the following comments are directed mainly to low-voltage utilization services. There are two fundamentals associated with the load-center principle of distribution. These are:

1. Power is distributed at high voltage (up to 15 kv) to the center of the load. There it is stepped down to utilization voltage (600 volts or less) and distributed to the load by short, low-voltage feeders.

2. Where the total load exceeds about 500 to 1,500 kva, several unit substations are used rather than a single large substation. Each substation is located near the center of its load area.

The old method, which is referred to here only in the way of contrast consists of a large substation in which large blocks of power are transformed from high voltage to low voltage in one area and then the power distributed to the load areas by long, heavy-conductor feeders as shown in Figure 3.

Figure 4 shows the load-center method of power distribution for this same plant. In comparing the over-all economics of the load-center and the old method of power distribution, the only true comparison is on an installed basis and including all components, such as the primary switchgear, primary cable, transformers, low-voltage switchgear, and low-voltage cable. Unless all of these are properly evaluated, erroneous comparisons will result.

The optimum size of substation, considering all of the foregoing variables and contradictory trends, is shown in Figure 5. It should be noted that the optimum size of substation for 460-volt systems ranges from 500 to 1,500 kva, which is smaller than was generally considered economical previously. Although these data are based on a load density of 10-volt-amperes per square foot of plant floor area, the optimum size substation for other load densities ranging from 3 to 30-volt-amperes per square foot still come within this same general range. These data also apply to 575-volt systems. The load-center system is applicable to all sizes of plants where a transformation of voltage is required.

The following is a summary of the advantages of the load-center method of power distribution when using load-center unit substations (described later):

1. Low installed cost of the entire power-distribution system.

2. Ease of installation.

3. Adaptable to future expansion or modernization or expansion of existing plants. Unit substations may be considered as building blocks to be added as the load increases. This avoids the investment of extra capital in substations larger than necessary based on the anticipation of future increase in load.

4. Excellent voltage regulation because of short low-voltage feeders.

Table I

Utilization-Equipment Voltage Rating	Generator or Transformer No-Load Voltage Rating		Nominal System Voltage Class
220 or 230	240	.....	230
440 or 460	480	.....	460
550	600	.....	575
2,300	2,400	.....	2,400
4,000	4,160	.....	4,160
6,600	6,900	.....	6,900
13,200	13,200 to 14,400	.....	13,800

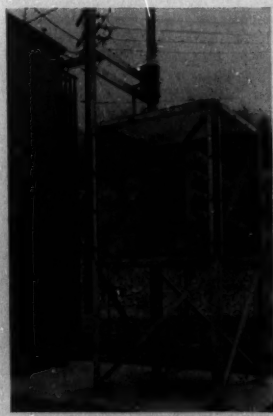


Figure 8. Outdoor stainless steel grounding resistor connected to neutral of a power transformer

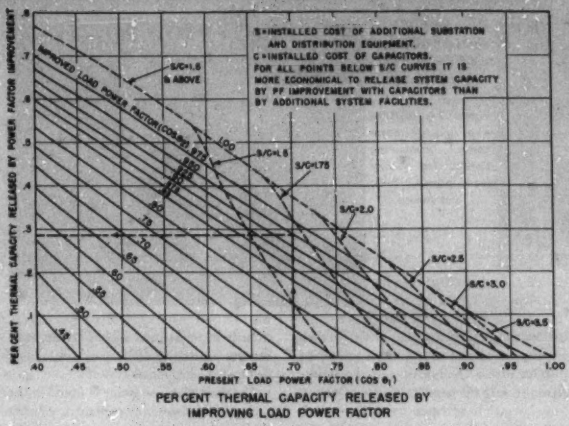


Figure 9. Per cent system capacity released by power-factor improvement and the economical limits of obtaining additional electrical capacity by capacitors

If the load power factor of a substation is improved from 70 to 95 per cent, the system capacity released is 28.5 per cent; that is, the system can carry 28.5 per cent more load (at 70 per cent power factor) without exceeding the net kilovolt-amperes before the power factor was improved

This curve also shows that it is more economical to obtain this amount of system capacity by power-factor improvement with capacitors than purchasing additional substation and distribution facilities for all S/C values above 1.7 (obtained by interpolation)

5. Simplified engineering.
6. High salvage value.

Circuit Arrangements for Low-Voltage Systems

The over-all electric service reliability of most plants is excellent, yet management of some industries, and usually the large plants, is becoming actively interested in improving even the present excellent record.

Some of the reasons which have influenced management in this direction are as follows.

1. In some industries production workers are paid for a full day if sent home due to circumstances beyond their control.
2. Excessive overtime pay for maintenance or repair work.

There are cases where both of these have influenced the circuit arrangement for greater service reliability.

The reliability of a power system is dependent upon proper maintenance, the use of high quality equipment, and the circuit arrangement. Assuming proper maintenance and high quality equipment, the circuit arrangement greatly influences the potential reliability of the system and is the principal factor over which control can be exercised in the distribution layout.

The types of low-voltage distribution systems applicable to most industrial power systems are the radial and the secondary-selective arrangement, although there are other arrangements.

The radial system, Figure 6a, characterized by a single primary channel and substation, is suitable for general service where a power outage is not especially detrimental. The merits of the radial system are low cost and simplicity. This is the most common power-distribution system and practically the only one in small plants.

The secondary-selective system, Figure 6b and 6c, is especially suitable for continuous process loads, boiler house auxiliary supply, and whenever the service reliability and operating flexibility require more than that offered by a straight radial system. Basically, it consists of two independent radial systems but interconnected on the low-voltage bus through a normally-open tie circuit breaker.

It should be noted that with the secondary-selective arrangement, service reliability is the result of the system arrangement in contrast with some methods where service reliability is dependent upon the reliability of the type of equipment.

Some of the advantages of this arrangement are:

1. Duplication of supply to the low-voltage bus, thus providing continuous service for the entire load area. The primary circuits are usually physically separated throughout their entire length, making it impossible that a fault on one circuit will be transferred to the other circuit.
2. Retains the simplicity of the radial distribution system. No special relaying is required.
3. All transfer switching is performed by full-capacity air circuit breakers, thus contributing to safety. Transfer switching can be accomplished automatically, if necessary, at very little extra cost.

Figure 10 (below left). Outdoor load-center unit substation with explosion-proof air circuit breaker units in a Mexican oil refinery

Figure 11. Double-ended, 1,500-kva load-center unit substation with Askarel-filled transformers supplying power to 460-volt services. (Secondary-selective arrangement as shown in Figure 6b and c

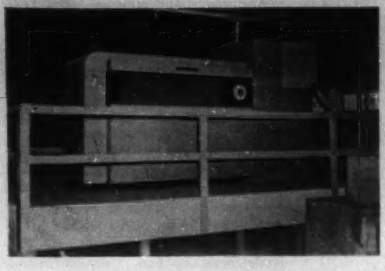


Figure 12 (left). Indoor load-center unit substation with dry-type transformer

Figure 14. Cabinetrol unit for motor control containing starters for boiler feed pumps, fans, and stokers

4. During normal operation, each substation shares in carrying the load, resulting in excellent voltage regulation and low system losses.

5. Well suited for separation of lighting and flicker-producing loads. Lighting and power can be supplied from one substation and the flicker-producing loads and power from the other substation. During normal operation the two busses are not in parallel so the flicker load will not affect the lighting.

The spot network arrangement, as shown in Figure 6*d*, is applicable where the highest degree of service reliability is necessary. However, very few industrials have adopted any of the network arrangements.

The following are representative relative costs, on an installed basis, of these various types of distribution arrangements for 460-volt service as applied to medium and large-size plants:

Radial—100 per cent (base comparison).

Secondary Selective—105 to 140 per cent, depending upon the reserve capacity of transformers.

Spot Network—115 to 150 per cent, depending upon the reserve capacity of transformers.

System Grounding

The subject of grounding industrial power systems has received considerable attention the past few years as evidenced by the numerous articles in engineering publications. Operators are beginning to appreciate the benefits of grounded system operation and the over-all trend is definitely to grounded operation in the 2.4- to 13.8-kv class (practically all new systems above 5 kv are grounded). A few of the low-voltage systems, that is, 600 volts and below, are also being grounded, but not to as great an extent as the higher voltage systems. Although past experience offers little guidance on grounding, nevertheless a review of some of the factors involved, such as voltage, safety, and cost, are interesting even though somewhat inconsistent.

Offhand it would be expected that operating voltage level would be a clue to grounding and also that the higher voltage systems would be grounded both for engineering and safety reasons, yet that does not appear to be true. For example, 115 and 120/208-volt systems such as house circuits, are grounded primarily for safety reasons, yet very few higher voltage systems such as 460, 2,400, and 6,900 volts were grounded in the older installations.

As far as cost is concerned, there is practically no difference between grounded and ungrounded systems up to 15 kv. It is only on main transmission circuits of 69 kv and higher where costs become an important factor and then in favor of the grounded system.

The most promising clue is found in the type of transformer or generator connection, ungrounded systems are usually associated with delta-connected equipment and grounded systems with Y-connected equipment.

Since the trend is towards grounded system operation, what are the reasons for this? The following are briefly the advantages of grounded system operation and although applicable primarily to the main power-distribution systems, they apply generally to low-voltage systems also:

1. Automatic and selective indication of ground faults in feeder circuits.

2. Greater safety to personnel.

3. Reduction of overvoltages.

4. Better over-all protection to equipment, especially generators, transformers, and motors protected with differential-type relays.

Since many plants still operate with an ungrounded system, it may be well to review that type of system operation. The main and about the only argument in favor of an ungrounded system is that a grounded circuit can remain in service until it is convenient to shut down the

Figure 13. Installation of complete load-center unit substation and cabinetrol unit substation for motor control in plant of western lumber mill

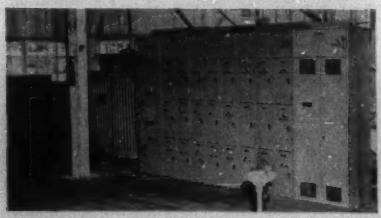


Figure 15. Installation of 2—400-kw 250-volt 2-wire d-c sealed-ignitron mercury-arc rectifier unit substations with Askarel-filled rectifier transformers

Figure 16 (left). D-c unit substation with dry-type transformer

Rectifier - cubicle door open showing mounting of rectifier tubes

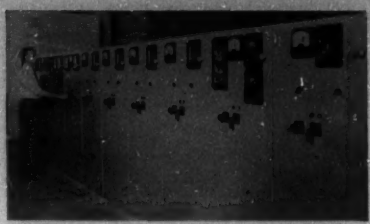


Figure 18. Metal-clad switchgear serving main 2,600-volt boiler house auxiliaries in a southwestern oil refinery

plant. The main disadvantages are the difficulty and cost of locating grounds.

Since the main reason in favor of an ungrounded system centers around continuity of service, that should be examined closely. Service continuity is very important for some industries; they will accept the change of trip-outs due to multiple grounds or damage to equipment due to overvoltage in the interest of maintaining service until a shutdown can be planned. However, there are very few industries in that category; most plants can tolerate a short shutdown and there are always nonessential services which can be temporarily de-energized to repair a faulted circuit.

In most plants it is the total down time which is important and, since ground faults are quickly located on a grounded system due to automatic and selective relaying, the down time is usually less than for an ungrounded system. This has been demonstrated in plants which have changed to grounded system operation and, significantly, it is rare to find a plant changing back to ungrounded system operation.

The magnitude of ground fault currents in the main distribution system is purposely limited to rather low values; generally in the range of 500 to 2,000 amperes or a value consistent with proper relaying operation. It is advantageous to limit the magnitude of the ground fault current to minimize damage to rotating machinery, cable, switchgear, or any other metallic parts in the ground path. Typical grounding equipments for industrial plants are shown in Figures 7 and 8. A discussion of the various grounding methods is beyond the scope of this paper.

Economic Advantages of Capacitors

Capacitors are a useful and economical tool which should have much more use in industrial plants. The principal reasons for their use are reduction of power costs and release of system capacity. There are other benefits, such as improvement in voltage level and reduction of system losses, but these may be considered as additional advantages.

The principal reason for using capacitors is the reduction of power costs when a power-factor clause or its equivalent is part of the rate structure. Actual plant reports show that capacitors usually pay for themselves in a period of six months to two years. This corresponds to a gross rate of return on the capacitor investment of 50 to 200 per cent!

Capacitors are an economical and quick way of releasing circuit capacity to take care of additional loads or reduce overloading. This factor is especially important now when capacitor shipments are short, generally from stock or up to 15 weeks for the types commonly used in industrial plants, as compared with much longer shipment for distribution equipment. The use of capacitors for this purpose was not too well understood or appreciated in the past and may account for the fact that they were not used more for this purpose. However, the war demonstrated the importance of capacitors and many of the war plants included capacitors in the power distribution system. Also, many of the existing plants found capacitors a quick and economical means of taking care of their increased load and in many cases was the only practical solution. Figure 9 shows the per cent system capacity released by power-factor improvement and the economical limits of obtaining additional electrical capacity by capacitors.

Liquid-Filled Versus Dry-Type Transformers

The two principal types of transformers used on industrial power systems are Askarel-filled and dry-type and each has its own applications.

Askarel is a synthetic liquid which is noninflammable and has been used in transformers for almost 20 years. Because Askarel is noninflammable the transformer can be located indoors without a surrounding protective vault, and it is because of this feature that Askarel-filled transformers have been so popular for indoor location.

Dry-type transformers have been avail-

Figure 17. Metal-clad switchgear for a 4,160-volt system in an industrial plant

The 4,160-volt feeders directly above the switchgear are interlocked-armored cable. Askarel-filled load-center unit substation at extreme right

Figure 19. 3,000-kva outdoor master unit substation supplying power to a coal mine

Figure 20 (left top). Outdoor master unit substation supplying power to a mid-western oil refinery

The 2,400-volt switchgear section includes motor starters and a feeder for miscellaneous 460-volt auxiliary power service

Figure 21. Installation of a limitamp high-interrupting capacity 2,300-volt motor starters in a paper mill

Figure 22 (right). Installation of a 460-volt small rack, dust-tight capacitor equipment commonly used in industrial plants

able for many years in small sizes and for low-voltage service (for example in lightning circuits). It was only in the last few years that they became available in the larger sizes and higher-voltage ratings for power distribution service. Since there is often a choice between Askarel-filled and dry-type transformers for load-center unit substations located indoors, Table II will serve as a guide for the selection of the proper transformer.

A dry-type transformer weighs less than an Askarel-filled transformer and this may be a deciding factor when the substation is to be located on a balcony or rafter.

Safety

The most important single item contributing to the safety of operators and other personnel is the use of metal-enclosed equipment. Practically all of the equipment components of high- and low-voltage power systems are metal-enclosed in the interest of safety. Typical metal-enclosed equipments include switchgear, unit substations, motor starters, transformers, capacitors, and bus run. Cable circuits are usually in conduit or equivalent or have an interlocked armored covering.

Mechanical-key interlocking is often used in the interest of safety to prevent improper operation of manually-controlled switches.

Danger of electrical shock to personnel, and especially to machine operators, can be reduced by proper arrangement of the plant-protective grounding system. This protective grounding system pertains to proper grounding of motor frames, and so forth, and is not to be confused with a grounded power distribution system as described previously.

Only when sufficient attention is given to the over-all protective grounding requirements is there assurance of an adequate and safe grounding system. This applies equally well to ungrounded or grounded systems and to high- and low-voltage power systems.

The protective grounding systems should have a direct path to ground in order to avoid unintentional transfer of ground currents among various metallic parts of the building and piping system, and to avoid differences of voltage between exposed metallic parts which might constitute a hazard to personnel.

The main grounding system should be interconnected with grounding paths via cable sheaths and plant piping. The plant piping system should be bonded to the steel of the building structure. The low-voltage protective grounding system to motor and starter frames, busway, transformers, and switchgear should be interconnected with the main grounding system.

Table II

Installation Conditions	Type of Transformer
Clean, dry conditions, free from lightning	Dry-type or Askarel
Dirty locations, oil vapors, chemical vapors, metal dust	Askarel
Wet locations	Askarel
Direct exposure to lightning	Askarel
Installed directly over machine tools	Dry-type or Askarel

Types of Equipment in Use

The present trend in power equipment is towards the use of factory-assembled unit-type equipment completely wired and ready for installation.

One advantage from the user's standpoint is the lower over-all cost, but there are others which will be mentioned later. This lower cost is the direct result of standardization and repetitive manufacture of component parts and assemblies. Standardization is not just an overworked expression, it is the real backbone of industry. Standardization of equipment by manufacture is just as desirable as standardization in manufacturing; the objective in each case is the same—a lower unit cost.

There is always the possibility of sacrificing flexibility by standardization. That is true if the over-all equipment is standardized. Electrical manufacturers of factory engineered and assembled equipments have provided adequate flexibility by standardizing component parts, not the equipment, and using these as building-blocks to meet practically any requirement. Take, for example, the

Figure 23 (left). Installation of 4,160-volt outdoor capacitor equipments such as used in large plants

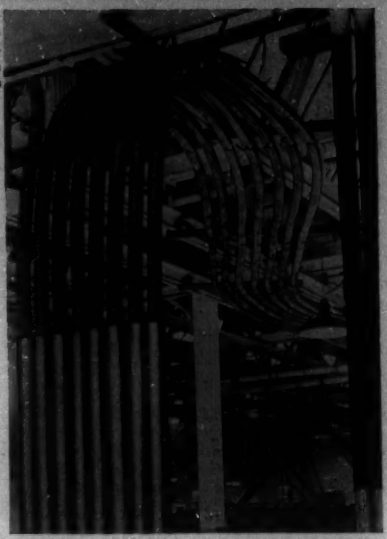


Figure 24. Flexible and simple means of mounting and suspending interlocked-armored cable

standardized load-center unit substations; they are available with several types of incoming line arrangements, in various primary and secondary voltages, dry-type or liquid-filled transformers, various circuit breaker ampere and interrupting ratings, and are applicable to various secondary-circuit arrangements.

Some of the advantages and features of standardized factory-assembled unit-type equipments are:

1. Lower first cost.
2. Lower installed cost, generally in the range of 15 to 30 per cent less than piecemeal components assembled on the job.
3. Ease of installation.
4. Safety. In general, equipments are totally metal-enclosed.
5. Flexibility. Additional switchgear units are easily added.
6. High salvage value.
7. Simplification of engineering.
8. Simplification in ordering.
9. Neat in appearance.
10. Desirable operating and maintenance features such as drawout-type circuit breakers in switchgear.

A-c and d-c unit substations are available with liquid-filled or dry-type transformers for indoor service and liquid-filled transformers for outdoor service. Generally, they are located indoors near their load in accordance with the load-center method of distribution in order to obtain the lowest over-all system cost. Some of these various types of factory-assembled equipments are shown in the following:

A-C Unit Substations. Figures 10, 11, and 12 are unit substations with ratings ranging from 100 to 2,000 kva and are available for the common primary and secondary voltage classes.

Motor-Control Unit Substations. (Figures 13 and 14.) These combine the functions of transformation and motor control in a convenient equipment and are available for various motor control functions. Where a suitable low-voltage power source is available the transformer is omitted.

D-C Unit Substations. (Figures 15 and 16.) The d-c unit substation fulfills essentially the same need for d-c power as the a-c unit substation does for a-c service. These substations include a primary breaker where necessary, transformer, sealed-ignitron mercury-arc rectifier, d-c switchgear, and the necessary auxiliary equipment. They are available in ratings of 75 to 500 kw for 250-volt service and 75 to 1,000 kw for 600-volt service.

Metal-Clad Switchgear. Figures 17 and 18 are installations of modern metal-clad switchgear (up to 15 kv) used for main power generation and distribution circuits. The power circuit breakers are removable.

Master Unit Substations. Figures 19 and 20 are substations used for transforming the voltage when the incoming supply and main plant distribution voltages are different. They are available in a wide range of voltage classes, kilovolt-ampere ratings, and incoming-line arrangements.

High Interrupting-Capacity Fused Motor Starters. Figure 21 is economical motor starters with current-limiting fuses, available for use on systems of high interrupting capacity. These motor starters can be used for systems with fault duties of 150,000 kva for 2,400-volt service and 250,000 kva for 4,160-volt service.

Capacitors. Figures 22 and 23 are typical illustrations of capacitor equipments for industrial use; the individual capacitor units are enclosed in a metal housing.

Interlocked-Armored Cable. Figure 24 shows a cable installation with interlocked-armored cable. The installed cost of interlocked-armored cable is only 50 to 75 per cent of the installed cost of a comparable quality cable in conduit. Although this cable is not applicable to all plant areas—depending upon the atmospheric conditions—it should be used whenever possible to realize minimum cost for the cable system. This type of cable is available, with various types of sheaths under the armor, for service voltages up to 15 kv.

Discussion

John S. Parsons (Westinghouse Electric Corporation, East Pittsburgh, Pa.): A paper such as this, which undertakes to give trends and practices, should be very careful not to give a misleading picture. Also, it should clearly differentiate between facts and the author's views and opinions on controversial phases of the subject.

In this paper many of the author's views on various phases of the subject are given as statements of facts. For example, the statement is made:

> "Based on the installed cost of all components comprising the distribution system, a 4,160-volt system is more economical than a 13,800-volt system for plants up to about 10,000 kva."

I have studied many cases where plants having about half that kva demand could be more economically supplied at 13,800 volts when all factors including losses were

evaluated. Also, I have found many cases where it was economical to supply much larger than 200 horsepower motors from a 460-volt system. When I say much larger, I mean motors of more than twice that size.

I note that the author would use the secondary-selective radial system where service continuity and flexibility are required greater than that provided by a simple radial system. In my opinion the primary-selective secondary network system will provide much greater service continuity and flexibility than the secondary-selective radial system and at very little increase in cost.

The author makes the statement "very few industrials have adopted any of the network arrangements." The facts are as follows: The first industrial secondary network system was installed in 1936, and a few more up to 1941. However, from the beginning of 1942 to date, more than 80 industrial secondary network system installations have been made, and a very substantial number of them have been purchased and installed since the war. The majority of these industrial network installations have been of the primary-selective secondary network system, a type not mentioned by the author. To me these facts indicate some trend toward the use of the various forms of industrial secondary network systems, and in my opinion, it is a trend which will increase in the future.

Central Utility Power Service Versus Local Plant Generation in Industry

A. H. FRAMPTON
FELLOW AIEE

ADEQUATE, secure, and economical electric power service is a vital part of our present-day economy. The solution to the problem of selecting this service, in many of the varied forms of industrial and commercial establishment, normally is found in the selection of central utility service, the supply of which generally has been entrusted to central electric utility companies which enjoy virtual monopolies in their respective territories.

However, further investigation of the problem of power service oftentimes is indicated in those specialized, and usually larger, industries wherein the supply of steam, at various pressures and in substantial quantities, is one of the basic factors of plant design, or wherein plant processes present opportunities for energy recovery that cannot be ignored. Such industries will include, among others, those engaged in the refining of petroleum products, in chemical processes, in paper production, ore reduction or refining. Certain forms of commercial establishment, such as hotels, larger office buildings and hospitals, may be included in the former category.

In these cases, economic and other arguments may be advanced in favour of an independent, local generating plant, co-ordinated with the steam demands of the industry or with its other energy recovery possibilities.

It is the object of the paper to present and discuss certain principles inherently involved in the study of such cases, and to indicate that the selection, design, and correlation of the various plant services requires a detailed knowledge of all the factors involved. It is concluded, that the correct solution will be reached only when these factors have been accurately weighed and placed in their proper perspective. Precedent may indicate the correct approach to the problems, but not always the correct solutions.

Specification for Power Service

Modern industry usually purchases its major apparatus and equipment on the basis of a specification which the apparatus or equipment is required to meet. The preparation of such a specification serves two useful purposes with which this discussion may be concerned. First, it clarifies the thinking of the purchaser regarding the nature, the limitations and the expected performance of the item to be purchased and second, it becomes a statement to the supplier as to the type of product he is expected to offer and to supply.

Electric power service, in the cases under discussion, is also a competitive adjunct of industry, to be supplied by the central electric utility, or from the independent local generating plant. It naturally follows that a specification, which provides a basis for the comparison of the competing power sources, would be of material assistance in selecting that service most suited to the needs of the enterprise at large. More particularly is this true when it is remembered that the power service may have an important bearing, in fact, in certain industries, may be a governing factor, in production costs.

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A. H. Frampton is director of engineering, The Hydro-Electric Power Commission of Ontario, Toronto, Canada.

Such a specification might be summarized as follows:

Dependability. The power service should be dependable and in so far as practicable, free from interruptions that hamper plant production and independent of natural forces and other conflicting influences outside the control of the industry.

Quality. The service should be of such quality as to provide satisfactory voltage, frequency, and other electrical characteristics necessary to the maintenance of established production standards.

Convenience. The service should be convenient and should not require, without adequate compensations in other directions, the establishment of trained staff, purchasing practices and other plant procedure foreign to the major objectives of the industry.

Adaptability. The service should be adaptable to the varying needs of the industry in its cycles of expansion or contraction, and should not create undue delays at the one time nor, at other times, give rise to burdensome charges that must be met during periods of low production.

Economy. Last, but by no means least, the service should be economical, measured in terms of both the direct and the indirect costs, as these may be reflected into the plant production.

Scope of Discussion

It is not possible to discuss each of these specification headings in the detail of fact and figure, nor to present formulas which will provide solutions for the infinitely varied problems this subject presents.

Furthermore, it would be difficult, in one paper, to cover the full possibilities of power generation and energy recovery that exist in the different industrial establishments throughout the continent.

In this latter connection, three principal fields exist in which the possibilities of the production of electric power in an independent local plant are presented. The first occurs in those plants whose processes

make available by-product fuels that can be used for the generation of power directly. The second occurs when it is desirable to use waste heat for the generation of steam, and when the steam so generated leaves a surplus over process requirements sufficient to warrant its use in electrical generation.

These two cases do not occur with great frequency and, in general, are of a nature which make the comparison of the economics a fairly straightforward process. The remarks which follow, under the respective specification headings other than "Economy," will be equally applicable in considering the design and service requirements of such plants, but further discussion of their economic factors will be omitted.

The third field is the one towards which this discussion is mainly directed, being that field in which industrial plant processes include large and relatively constant demands for steam at moderate pressures. In these cases, there is presented the opportunity of generating steam at pressures and temperatures higher than required for process, passing it through a turbine to produce electric energy, and supplying it to the processes at the necessary conditions, as exhaust.

From the casual consideration only it frequently may be concluded that attractive economies are possible. More detailed and accurately weighed analysis may indicate that such economies are more than counterbalanced by factors difficult to evaluate in monetary terms.

This discussion, therefore, will treat with each of the specification headings in general terms, in an attempt to bring out various viewpoints, leaving it to those concerned with specific problems to attach to each item the value or the weight which, in the light of the surrounding circumstances, appears justifiable.

Dependability

An adequate and acceptable standard of service security to its customers is one of the first principles of the modern electric utility system. During the recent war period, and while the utility industry is recovering from the demands of war and struggling with the problems arising from an unprecedented postwar load growth, its standards of service necessarily have suffered, to some extent. Nevertheless, this is but a temporary period, and its problems will be overcome. Serious and prolonged interruption of utility power service is an event of such infrequent occurrence that, when it occurs, it forms the material of newspaper headlines. Reserve generating equipment, reserve transmission and other power distributing facilities, adequate maintenance and overhauling of equipment during off-peak periods ensure the protection of power supply against all but the extremes of natural forces which occur so infrequently as to be uneconomical to guard against.

It must be granted, therefore, that the utility system can maintain a dependability rating as high as required by all but certain specialized enterprises, for which extremely rigid specifications must be established. The degree to which this standard of dependability is made available in individual cases will depend, to some extent, on the nature of the immediate supply facilities, but, at the most, a premium rate would ensure such additional protection as duplicate line supply or supply from alternative sources.

It is true that an independent local plant can be arranged to provide equally dependable service but, nevertheless, it appears that this factor is often discounted in the desire to secure apparently greater economy. Single generating units, for instance, may establish excellent records of continuous service, but the threat of a major plant shutdown is always present and occasionally occurs.

Accurate comparison of the alternative possibilities in this particular is made only if the local plant provides the standby and other provisions necessary to a high-grade service factor or, conversely, if the economic comparison provides a due allowance for the loss of production contingent upon the hazards to continuous service inherent in such design of the local plant as may be finally selected.

Quality

Again, the modern utility system is designed and so operated and managed to provide to its customers a service having high quality electrical characteristics. Temporarily again, these standards have become difficult to maintain, but over the long-term period normal characteristics are established and maintained by automatic and other equipment which approaches perfection in its operating characteristics.

Frequency is controlled to a degree that permits most manufacturing operations to be timed electrically, even those of such exacting requirements as paper making, nylon production and various chemical reactions. Voltage variations beyond normally acceptable limits are occasioned usually only by infrequent equipment outages.

The independent local plant may meet equally acceptable standards, providing the proper controls and so forth, are incorporated in its design. However, in exacting cases, where, for example, severe load swings are created by such as welding equipment, voltage flicker, or frequency problems may be important, or their solution difficult, involving control equipment whose adjustment and maintenance require highly qualified personnel.

Again, in the ultimate comparison, the allowable credit under this heading should be given the utility service or, in the converse, recognition made of the degree of service contemplated from the local plant, a recognition which may not be easy to reduce to a simple monetary figure.

Convenience

Convenience in the electrical power service may be stated to be as important to modern industry as convenience in the supply of its raw materials and other components entering into its manufacturing operations. From this viewpoint, this factor in our specification, not withstanding its seeming minor importance, becomes not infrequently the one factor on which the final decision hinges.

If the industry, or commercial establishment numbers among its normal staff, trained mechanical and electrical personnel, and if its purchasing requirements already include coal or other fuel, and if other governing considerations discussed later are present, then the installation of an industrial power plant, independent of central station service, may be viewed in one light. If the reverse is true, and the installation of an independent plant requires the establishment or major augmenting of a power department, executive policy may dictate the foregoing of such economies as may be indicated, in the interest of the major effort of the industry.

The industrial power plant inevitably increases invested capital and brings with it personnel and supply problems, which are avoided by central station service. Modern industry specializes, and frequently is content to place its power supply in the hands of the specialists in this field.

Adaptability or Flexibility

Industrial markets are never static, and fluctuate, to a greater or lesser extent, with the general economic condition of the community. It is desirable, therefore, that production costs be capable of variation in suitable ratio to production output. To this end, the power service

should be capable of simple and convenient expansion as required, but should also be capable of equally simple and convenient contraction in its cost aspect, at least.

Under normal conditions, central utility service would appear to meet this specification in both particulars. Expansion of supply is often as simple as making a telephone call to the power department of the utility. Contraction may go a long way before being limited by basic minimum charges.

The local industrial plant has decidedly different aspects. Once designed and constructed, its future revision almost certainly will be difficult. Not only will the desirability of revision be dictated by the simple expansion or contraction of the industry, but also, as so frequently happens, by changes in plant processes which alter the relationship of the steam and power demands and thus upset the basic factors on which the original design was based. It must be granted, therefore, that the local plant is less readily expanded to meet increasing demands, and certainly cannot be contracted, if production requirements decrease, except to the extent of fuel and possibly labour savings. Furthermore, it is susceptible to being placed in a substantially less favourable position than originally contemplated with variations in the plant processes. The newer the industry, the more recent the production process developments, the less certain the markets and the greater the hazards that should be recognized in basing production operations entirely on an independent local plant.

Economy

The comparison of their relative overall annual costs is obviously the key factor in comparing central utility service with the local generating plant. Such a comparison has the value of concrete figures, but the establishment of correct figures is not simple and, in fact, may be surrounded with difficulties such that erroneous comparisons often are submitted in perfectly good faith.

On the surface, the comparison appears simple. Central utility plants consume on the order of one pound of coal per kilowatt-hour generated, a consumption which is effectively increased in the inverse ratio of the efficiency of the transmission and distribution system. On the other hand, the local "by-product" plant, operating as described, with the exhaust steam utilized as process heat, will consume probably not more than four-tenths of a pound of coal per kilowatt-hour generated, the loss in the condensing water of the heat of vaporization being avoided. It would appear, therefore, that as at least 60 per cent of the coal is saved, the local plant must be the most economical.

A correct approach to the problem recognizes the existence of two separate problems; first, the supply of the required process steam and, second, the coincident supply of the necessary electric power. The cost of supplying process steam would be established on the basis of the minimum plant arrangement required to provide steam at the process pressures and temperatures. The additional cost of the "by-product" electric power supply then would include all the additional costs, above the basic minimum, required to provide the power generating facilities.

These additional costs would include the added capital costs in the boiler plant for higher pressure and temperature equipment, the capital costs of power generating equipment, the value of additional space requirements, together with all additional operating, maintenance and fuel expenses. These capital costs naturally would be affected by the degree to which the previous specification factors are to be recognized in the plant design, in the form of standby boiler plant and turbine equipment, automatic protective and control features and the operating costs by the calibre of personnel required to operate and maintain the plant components.

The designer, undertaking the problem of establishing these respective costs, first is faced with the problem of establishing the plant demands. This will not be simple, even though the plant appears to be identical to an already operating counterpart. Furthermore, it will almost invariably be found that a suitable balance does not exist between the process heat and electric power demands. Under such circumstances, the ultimate efficiency will be unattainable and a compromise arrangement imposed upon the designer, by which the maximum anticipated steam demands can be met and also, at noncoincident intervals, the maximum electric load.

This compromise requires the selection between two undesirable alternatives. Either steam must be generated at intervals at the higher pressure and temperature and passed through reducing valves with consequent loss of its energy generating possibilities, or a condensing section added to the turbine, requiring peak electrical generation in a condensing cycle and, in addition, the continuous wastage of steam through the added section, in order to maintain satisfactory turbine temperatures.

The number of combinations open to the designer in such circumstances depends primarily on the designer's ability to select, in advance, those which appear to offer the greatest promise. At the best, the plant arrangement will be such that maximum efficiency is attained only under one selected condition and, at all other conditions, some sacrifice of the theoretically possible gains must be accepted.

The extent of these sacrifices again depends on the accuracy of the demand estimates. There may be cases in which the steam and power requirements are so well established that forecasts of the future demands can be made with unfailing accuracy. Unfortunately, however, in the great majority of cases, this cannot be true, as management adjusts the production pattern of the industry to meet the fluid conditions of competitive enterprise.

Even the factor of seasonal variations will act to upset previously well-laid plans. No matter how long weather records have been maintained, there always seems to come a time when the unprecedented happens, and with it an entirely new relationship between, say, steam for process heating, and electrical demands for pumping cooling water.

It is then that the plant arrangement, which appeared so favourable on paper, often presents to management a balance sheet vastly different from that anticipated, in which the estimated economies have been partially, and frequently entirely, eliminated. In this connection, the author is aware of at least one plant in which, for months each year, conspicuous clouds of steam, at process pressures, are blown to the atmosphere, being uneconomical to salvage even though each pound of steam carries with it a thousand or more British thermal units of heat from the fuel the designer was attempting to save. Presumably, countless other and similar situations exist elsewhere.

It may seem on the surface that when such conditions exist, they could be corrected. Often they can, and are, but, when the original installation is completed, it frequently is impossible to make the changes necessary to offset changing requirements without plant revisions that cannot be justified economically.

In addition to the factors of cost that depend primarily on the steam and power generating facilities, the economic comparison must take full cognizance of other cost items, sometimes less readily apparent. One, for example, is the space requirement of the generating plant.

When the industry is located in a restricted area, the available space obviously is most economically utilized for the main production processes. The intrinsic value of the actual ground area may be relatively low, but, as part of the productive plant area, particularly if it represents the possibilities for future expansion, its actual value may be many times higher. This must be reflected in the economic comparison.

Other factors appear in the plant auxiliaries. For example, the higher pressure installation required in the production of by-product power may necessitate a complete revision of the methods of returning condensate to the plant or, in the alternative, a much more extensive and more costly feed-water treatment. The operating staff, entirely competent to operate a medium pressure installation, may be a definite hazard in attempting to operate a 1,200-pounds per square inch plant, requiring replacement with more qualified and higher paid personnel.

Not the least important factor in establishing correctly the economic balance sheet will be the proper allocation of costs between the respective production departments. The annual costs of a simple boiler plant, operating solely to provide process steam, probably are readily subdivided in proportion to the respective departmental demands.

When by-product power generation is added, the correct subdivision of total costs may be less apparent. For example in such a case, the steam generated at the higher pressure and temperature will be used by the power plant itself for feed water heating or other auxiliaries, will be used by the turbine plant for by-product electric generation, and may be used by certain plant processes through reducing valves. The electric-power output will be used by the production departments, and maybe also by the power plant for the electric auxiliaries, perhaps also by the water pumping plant supplying condensing cooling water. The water pumped by the pumping plant also may be utilized in two or more different departments. It will be evident that, where such complications exist, careful analysis of the respective costs will be required, if the balance sheet is not to indicate substantial savings in one direction and overlook possible increases in cost in others.

In summary, therefore, the economic comparison of the competing sources will need to be made with a full appreciation of all the factors involved. It must recognize that one plant only can be built, and that that plant will be a compromise, attaining its full economic possibilities only under one operating condition. Future changes in that plant will be difficult, so that long-term advantages will be secured only to the extent that the long-term requirements have been accurately forecast.

All of its cost items, including those of providing service standards comparable to the utility supply, must be included or, in the alternative, due allowance made for the hazards accepted in designing for lower standards.

The allocation of these over-all costs must be made in such manner that evident savings do not mask hidden losses in other phases of production operations.

Joint Operation of Local Plant With Utility

It might be concluded from the above discussion that local plant generation seldom can be proved economical, or that its adoption is so fraught with difficulties that the economies inherent in by-product power generation should be forgotten, in the interests of playing safe. This, however, is not the author's thought.

It is intended primarily to discuss as many of the viewpoints involved as space permits, in the interests of a correct approach to the problems. In this connection, an alternative is frequently presented, which often merits serious consideration, namely, the joint operation of a local industrial plant with the system of the adjoining utility.

Perhaps the most obvious situation where joint operation may be of advantage is when the normal process steam requirements are insufficient to generate the full electrical demands in by-product turbines. In such cases, the independent local plant is forced to add condensing sections to its turbines, and to generate the additional electrical demands in a condensing cycle.

It may still be, that an apparently satisfactory economic comparison is derived by combining all electrical generating costs in one figure, but a more correct comparison might indicate that part of the savings effected by by-product generation are offset by the additional costs of the condensing generation. In other words, that the local plant can economically supply up to the amount of its noncondensing generation, but that the utility most economically supplies the balance.

In certain industries the reverse situation may obtain, in that the steam demands are more than sufficient to generate the electrical requirements. Here, an excellent opportunity is presented for cooperative effort. Noncondensing generating capacity may be installed to utilize the full steam demands, the surplus output being sold to the utility, in return for which the utility, as well as paying an equitable rate for energy purchased, provides to the industry such valuable service factors as standby capacity, voltage, and frequency control.

These latter items naturally are of value whenever parallel operation with the utility is established. Under suitable contractual arrangements, the local plant can avoid expensive additional installations for standby automatically receives the benefit of the voltage and frequency regulation provided in the utility system, and is protected, in large measure, against the vagaries of future changes in power demand created by plant expansion or process changes.

In conclusion, it should be stated that, in each case where the comparison of a local plant with central utility service is undertaken, a new set of conditions will be presented. There is no absolute scale against which any proposal can be checked, the final evaluation depending almost entirely on the ability of the engineer to determine the limiting factors, and on his judgment in presenting them in their correct relationship. Unless the designer is absolutely honest with himself, he may be in the position of having made an entirely wrong decision, one which will remain as a monument to his errors of judgment.

It seems desirable that for any new industrial project there should be a specification for the power supply at least as rigid as those for the major items of equipment. The minimum requirements of such a specification would include the factors set out and discussed above; in certain cases, others might be added to cover specialized requirements.

In considering the means by which a power service meeting this specification is to be provided, by-product power generation may prove attractive in those industries requiring large quantities of low- or medium-pressure process steam. The decision concerning the extent to which the savings inherent in this method of operation can be realized should be made only after the most careful investigation, if the apparent gains are not to prove illusory.

Joint operation with the utility company, in many cases, will permit full attainment of these inherent economies. At the same time, it will provide other advantages, not the least of which will be those intangible values difficult to reflect into a purely monetary comparison.

Discussion

S. E. Schultz (Bonneville Power Administration, Portland, Oreg.): Generally I have found it worthwhile to review Frampton's technical papers inasmuch as they are based on intimate operating experience. This paper is no exception.

The need for close co-operation between the industrial engineer and the central station engineer cannot be overemphasized in any endeavor wherein the comparative merits of central station power and local generation are considered. It is important that the industrial engineer bring his problems to the central station engineer as early as possible.

He should carefully weigh all of the intangible as well as the tangible factors involved and present recommendations to his management that fully take into account the obvious advantages of central station power. On the other hand, the central station engineer should not be overly zealous in presenting the case for central station power.

As Frampton points out, local generation at times may prove feasible in an industrial plant where a large amount of processing steam is required for manufacturing purposes. There have been other cases in the Northwest where other factors have served as an inducement for local generation. These include:

1. Availability of low-cost fuel, particularly in large lumber mills.
2. Where service over a single circuit transmission line may not supply sufficient reliability.
3. Where the industrial plant is located near a low-cost hydroelectric site.

With due regard to the advantages of central service, the Bonneville Power Administration in its dealings with large customers has not overlooked the possibility of mutual benefits from local generation. Such generation has served the purpose of meeting the emergency needs of an area, even to the extent of shutting down some of the industry's own operations and of providing energy during a critical period.

Induction and Dielectric Heating

G. W. SCOTT, Jr.
MEMBER AIEE

WHEN an electric current passes through a conductor, heat is generated according to the equation

$$H = kI^2Rt \qquad (1)$$

where

H = calories of heat
k = 0.239
I = current through conductor in amperes
R = resistance of conductor in ohms
t = time in seconds

This equation is the basic law governing such heating by an electric current, known as resistance heating. It provides a very convenient and simple means for heating electric conductors, for the most part metals, that have been used very extensively for scientific, domestic, and industrial purposes. Although resistance heating is in most instances more expensive than heating by coal, oil, or gas, the added cost is frequently compensated for by the added simplicity and efficiency inherent in this type of heating. Examples of the widespread use of resistance heating are electric toasters and coffee makers in the home, electric furnaces and electrically-heated press platens in industry, and a host of specialized uses in scientific work.

Despite the very great convenience of heating merely by making suitable connections to a source of electric power, resistance heating has certain limitations, a number of which can be overcome by other types of electric heating. Unless the object to be heated can be connected directly to the source of power, one must rely upon conduction and/or convection in order to transport the heat to the desired point. If this is the case, resistance heating offers only convenience over and above heating between steam heated press platens or in a convection oven. Frequently this factor is not sufficiently important to justify the higher cost of resistance heating for a particular heating application. Furthermore, if the object to be heated is a nonconductor, resistance heating is of no use whatsoever. Fortunately, however, there are forms of electric heating that present advantages over conduction and convection methods and that even permit the heating of electrical insulating materials at rates and to temperatures of industrial importance.

In order to present an advantage over conventional oven methods of heating in cases where the charge to be heated cannot be connected directly to the source of power, a special case of resistance heating known as radiant heating may be employed. Whenever a temperature differential exists between two bodies there is a direct transfer of energy by electromagnetic radiation from the body at higher temperature to the one at lower temperature in accordance with Stefan's law which states that

$$E = \sigma(T^4 - T_0^4) \qquad (2)$$

where

E = energy radiated in watts
σ = Stefan's constant = 5.7×10^{-12} watts centimeter^{-2} degrees^{-4}
T = temperature of radiating body in degrees absolute
T_0 = temperature of receiving body in degrees absolute

The energy thus transferred heats the receiving body.

It is apparent from equation 2 that the transfer of energy and, therefore, the rate of heating by this method vary as the fourth power of the temperature differential. This implies that the rate of radiant heating increases very markedly with the temperature of the source. If the source is the filament of a lamp heated by the resistance method to incandescence (2,500 degrees Fahrenheit) or an open coil heated to a bright red color (1,500 degrees Fahrenheit), conditions for the transfer of infrared energy to a body at room temperature are very favorable. Such infrared lamps and resistance coils are very widely used for radiant heating in industry. A particularly worth-while application both from the viewpoint of reduced costs and improved quality is the drying of paint on an automobile body by passing it through a radiant heating tunnel. The most significant feature of radiant heating is the speed with which it may be accomplished. Since the energy is transferred from the source to the object under treatment with the speed of light (3×10^{10} centimeter per second) and is absorbed instantaneously, it is unquestionably one of the most rapid means of heating available. However, it must not be overlooked that the radiated energy under very nearly all conditions is completely absorbed in the first few thousandths of an inch below the surface. Consequently, radiant heating is a surface phenomenon. If it is desired to heat below the surface layers by this method, conduction of the heat to these regions must be relied upon. Although this does not drastically increase the time element involved in radiantly heating a thick section of metal, it seriously limits this method for heating uniformly throughout the volume of a nonmetallic material. In order to accomplish these purposes successfully and economically, the two remaining forms of electric heating must be employed. Induction heating provides a means of heating metallic objects at speeds comparable to those possible with radiant heating as well as more uniformly than with the latter method, while dielectric heating provides the only means of heating nonmetallic materials uniformly at high speed.

If an electric conductor, preferably a magnetic material, is placed within the turns of a coil through which an alternating current is passing, the metal is

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G. W. Scott, Jr., is with the Armstrong Cork Company, Lancaster, Pa.

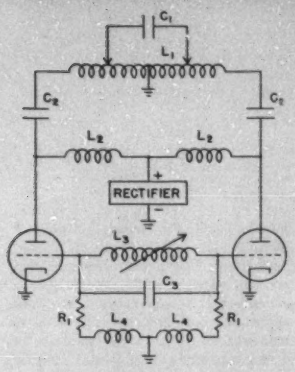


Figure 1. Schematic oscillator circuit for high-frequency heating

R_1–grid bias resistor, C_1–plate tank capacitor, L_1–plate tank inductor, C_2–plate blocking capacitor, L_2–radio frequency choke, C_3–grid tank capacitor, L_3–grid tank inductor, L_4–radio frequency choke

heated by magnetic induction, which effect is normally referred to as induction heating. The greater portion of the heating in all cases is due to the eddy currents induced in the metal in accordance with Lenz's law. Magnetic hysteresis accounts for a small part of the heating in magnetic materials such as iron and steel when below the Curie temperature. The heating caused by the eddy currents is simply resistance heating in accordance with equation 1. An approximate formula for the heat generated in a metal by induction indicating the variables involved is

$$p = \frac{H^2\sqrt{\rho\mu f}}{8\pi} \qquad (3)$$

where

P = heat generated
H = tangential component of magnetic flux at surface of charge
ρ = resistivity of charge
μ = magnetic permeability of charge
f = frequency of current

The depth of penetration of the eddy currents in the metal is determined by the frequency of the source of power; the higher the frequency the less the penetration. This phenomenon is known as "skin effect." Variation of frequency in relation to the geometry of the charge allows the uniformity of heating throughout the volume of the charge to be controlled. If certain nonconducting materials are placed between two electrodes across which an alternating voltage in excess of one megacycle is impressed, a heating of the material will take place by dielectric loss. In the presence of the high frequency alternating electric field, polar molecules within the nonconductor are continually reoriented with variations in the electric field. By this behavior energy is dissipated as heat and an effective current in phase with the applied voltage may be considered to flow between the plates. The heating effect of such a current is governed by equation 1, the resistance term for which is the effective series or parallel resistance of this imperfect capacitor formed by the electrodes and the material to be heated. The heat generated between plane parallel electrodes in dielectric heating is governed by the equation

$$W = 5/9k \tan \delta f(E/d)^2 \times 10^{-12} \qquad (4)$$

where

W = heat in watts per cubic centimeter
k = dielectric constant of charge
$\tan \delta$ = dissipation factor of charge
f = frequency of power source in cycles per second
(E/d) = voltage gradient between electrodes in volts per centimeter

Induction and dielectric heating have in recent years been put to some very unusual and worthwhile industrial uses. It is felt that these unique forms of electric heating, can play a very important role in the program of industrialization now going on in Mexico, particularly in view of the additional electric power that is anticipated in that country. For this reason the remainder of the paper will be devoted to a detailed treatment of the theory and practice of induction and dielectric heating. Already the relationship of these methods to the other forms of electric heating has been illustrated.

Equipment

Induction and dielectric heating are both a-c effects. There, however, the similarity ends. Even in the matter of frequency there is considerable disparity between the two forms of heating. Induction heating is possible from 60 cycles upward to a few megacycles where lack of penetration makes it impractical, whereas dielectric heating becomes appreciable at about one megacycle and is improved with progressively increased frequency until tuning difficulties, standing wave effects, or availability of power render it ineffective.

Up to 9,600 cycles rotary generating equipment is satisfactory and available for induction heating. From this point to approximately 500 kc, spark gap oscillators may be employed. Above 500 kc it is necessary to employ a vacuum tube oscillator for both induction and dielectric heating. Such oscillators are readily available from United States manufacturers with output power up to 125 kw. This equipment is quite similar to that employed in radio broadcasting so that the radio frequency heating industry has benefited considerably from equipment development work done during the last two decades in the former field. The cost of this equipment will be analyzed later.

A simplified circuit diagram of an oscillator for radio frequency heating is shown in Figure 1. For both induction and dielectric heating, a common requirement is that the load to be heated be tuned to approximately resonance with the output of the oscillator so that power may be most efficiently absorbed from the latter. Figure 2 *A* shows a representative load tuning circuit for induction heating employing a series resonant circuit while Figure 2 *B* shows the same for a dielectric heating application utilizing a parallel resonant circuit. In either induction or dielectric heating, the charge to be heated may be placed in the proper component of the oscillator resonant circuit. However, this is not always as satisfactory as a separate tuned load circuit because of resulting instability in oscillator performance in the former instance.

Induction Heating

Induction heating is a very old phenomenon which was probably first seri-

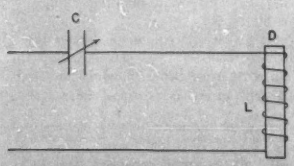


Figure 2(A). Load tuning circuit for induction heating

C–tuning capacitor, L–work coil, D–charge

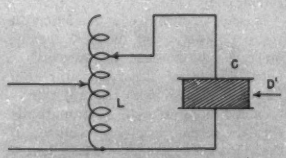


Figure 2(B). Load tuning circuit for dielectric heating

L–tuning indicator, C–heating electrodes, D'–charge

ously encountered industrially for core losses in motors and transformers. Such losses in these instances were minimized by means of laminated core design, but have later been put to use for industrial heating.

A cylindrical metal charge D to be heated is shown in Figure 3 A within a coil L and placed symmetrically with respect to the axis of the coil. When an alternating current I flows through the coil L a magnetic field H parallel to the axis of the coil is produced as shown in Figures 3 A and 3 B. Once during each cycle the magnetic field H_1 or H_2 collapses and springs up in the opposite sense, inducing in the charge the eddy currents i_1 and i_2 as shown in Figure 3 B. Such currents heat the charge by the resistance effect in accordance with equation 1.

Furthermore energy is also dissipated in the charge from magnetic hysteresis, provided the magnetic permeability μ of the charge is sufficiently high. However, since $\mu = 1$ for all except ferromagnetic materials such as iron and steel and for all materials when above the Curie temperature, the heating by hysteresis is secondary in most instances to that by the induced eddy currents.

For convenience of representation the heat produced by hysteresis may be considered due to additional current flowing in a path parallel to the induced eddy currents. If this is the case, the equivalent circuit for the arrangement of Figure 3 is shown in Figure 4. The heating effect in the charge, of course, is proportional to i^2R.

Although a qualitative understanding of induction heating is simple to grasp, a solution of the differential equations representing the situation is involved and beyond the scope of this presentation. An approximate equation[1] for the energy dissipated per unit length of charge is

$$P = \frac{8\pi H_0^2}{\sigma} \cdot \frac{r}{d} \tag{5}$$

where

P = energy in watts per unit length
H_0 = impressed magnetic field in ampere turns per centimeter
σ = conductivity of charge in ohms per cubic centimeter
r = radius of charge in centimeters
d = depth of penetration of heating in centimeters

Of course the depth of penetration of heating is controlled by the skin effect and may be expressed as[1]

$$d = 2\pi \frac{1}{\sqrt{\mu \sigma f (10^{-9})}} \tag{6}$$

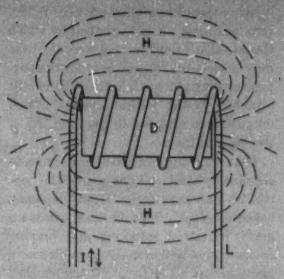


Figure 3(A). Induction heating work coil

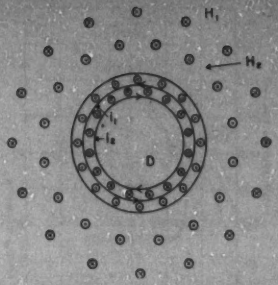


Figure 3(B). Magnetic field around and eddy currents within an induction heating charge D

in which

d = penetration of heating in centimeters
μ = magnetic permeability of charge
σ = conductivity of charge in mhos per cubic centimeter
f = frequency of power source in cycles per second

Equation 5 is accurate for practical purposes when $r/d > 5$, that is when the penetration of the heating is no greater than 20 per cent of the distance from the surface to the center of the charge.

Although equations 5 and 6 show clearly the variables upon which the rate and control of induction heating depend, they are not useful for more than an approximate prediction of rate of temperature rise in a charge being heated. An important factor contributing to this fact is that the material variables σ and μ are both a function of temperature. In fact the permeability μ of magnetic materials drops abruptly to unity when the charge reaches the Curie temperature, causing a marked change in the slope of the temperature rise curve.

In Figure 5 are plotted power and temperature data taken during an induction heat treating cycle on a 20-millimeter armor piercing shot. It is observed that at the beginning of the cycle the rate of power absorption P increases as the temperature rises due in part to the decrease in conductivity of the metal charge at elevated temperatures. However, at the Curie temperature the magnetic permeability μ drops abruptly from a value of the order of 1,000 to 1 causing the considerable decrease in power absorption shown in curve P of Figure 5. This change in power absorption is reflected in the curve T for temperature rise in Figure 5.

Above the Curie temperature of the metal, the rate of temperature rise levels off. Practically, such a sharp change in material characteristics may be compensated for by retuning the load circuit, resulting in the temperature rise curve T_1 in Figure 5.

Up to this point the induction heating charge considered has been a cylinder placed symmetrically within the turns of a work coil. It should be emphasized that symmetry is not an inherent requirement of the method. A great variety of work coils of different shapes have been utilized to heat nonsymmetrical charges or to localize the heating in the charge as required. In the design of such coils it should be kept in mind that the efficiency of the energy transfer is determined in large part by the closeness of the inductive coupling between the charge and the work coil, that is, the factor M in Figure 4.

Although restriction of the penetration of heating by the skin effect to within a few thousandths of an inch of the surface in some instances is very desirable as in an application such as surface hardening, it is not adapted to the gross heat treating or melting of metals. It is apparent from equation 6 that the penetration d is inversely proportional to the square root of the frequency of the power source. Therefore, for a charge of specified cylindrical shape, there is a maximum frequency for

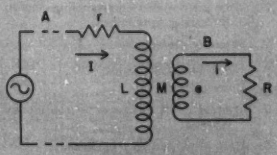


Figure 4. Equivalent circuit for induction-heating load

A—circuit of work coil, B—circuit of charge, M—mutual inductance between A and B

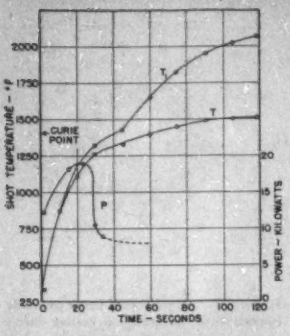


Figure 5. Power absorption and temperature rise during induction heating cycle

optimum over-all heating as expressed below.[3]

$$f = 7.9 \times \frac{\rho}{\mu r^2} \times 10^7 \qquad (7)$$

where

f = frequency in cycles per second
ρ = resistivity of charge in ohm-centimeters
μ = magnetic permeability of charge
r = radius of cylinder in centimeters

Quantitatively, equation 7 requires that the larger the cylinder to be heat treated, the lower the maximum allowable frequency.

Applications of Induction Heating

There are countless specific applications of induction heating in industry at the present time. These applications may be summarized under the following general headings:

1. Melting—flow of tinplate
2. Annealing
3. Heat treating
4. Surface hardening
5. Outgassing vacuum tubes
6. Soldering and brazing

The particular advantages of induction heating over conventional methods are:

1. Very great speed—power inputs up to 80 kw per square inch
2. Precisely controllable
3. Differential heating possible
4. Convenience
5. Contact not necessary—continuous process
6. Heat generated only in charge

The advantages from induction heating enumerated make possible certain of the applications listed just previously and result in cost reduction or quality improvement in the others. For instance induction heating is the only method by which the metallic elements of a vacuum tube may be elevated to a red heat for the purpose of outgassing while the tube is still attached to the vacuum pump. Such outgassing is essential to the long life of the vacuum tube.

The precise localization of heating made possible by work-coil design and the control of heat penetration from frequency variation greatly improves and speeds up many industrial soldering and brazing operations as well as makes possible many many others. The fact that the transfer of induction heat is essentially instantaneous and requires no direct contact with the charge makes it possible for certain of these brazing and soldering operations to be carried out continuously in production. Tin can be melted on black iron sheet at such a rate by induction heating to make possible the production of tin plate continuously at speeds as high as 1,000 feet per minute. This operation requires a frequency of 200 kc and an output of 1,200 kw.

The skin effect in induction heating makes it possible to surface-harden rapidly an object with irregular surface contours such as a gear. Furthermore, when the surface of a gear or other metal object has been raised to a red heat in a few seconds and the power immediately discontinued, the flow of heat inward produces an automatic self-quenching effect. In addition, the very great rates of heating and quenching possible with induction heating make possible the formation of metallurgical structures otherwise very difficult to attain.

In the face of the considerable advantage offered by induction heating it must be borne in mind that this method is inherently expensive and is normally not justified if other methods provide equivalent results. If the heating may be accomplished at 60 cycles, or normal industrial power frequency, the increased cost is only that of electric power over other available forms of power. However, if higher frequencies are demanded it is necessary to expend capital on special motor generator or oscillator equipment. In the latter case, the efficiency of utilization of the electric power is normally not greater than 50 per cent. More will be added regarding the cost factor after the discussion of dielectric heating.

Dielectric Heating

The first instance of dielectric heating probably took place in connection with the Leyden jar experiments conducted in Holland in the middle of the eighteenth century. The glass of the jar was heated by dielectric loss when the metal foils surrounding the jar were charged, producing an electric field in the glass. Since that time the phenomenon has occurred continually as a nuisance in the form of capacitor losses. Not until about ten years ago was dielectric heating established as a potential means of industrial heating.

In order to accomplish dielectric heating of a charge of normally insulating material such as wood, rubber, cork or glue, the charge must be placed between the plates of a capacitor C tuned with an inductance L as shown in Figure 6. The tuned load circuit is connected by a transmission line T to a power oscillator E operating at a frequency in excess of one megacycle. The electric field thus established across the capacitor causes the heating in the charge between the capacitor plates. The combination of C and D form an imperfect capacitor, and a statement of the theory of its behavior will determine the variables responsible for dielectric heating.

In an imperfect capacitor there is a component of current in phase with the applied voltage as well as the component 90 degrees out of phase, which is responsible for the dielectric heating. This in-phase current in a normally insulating or dielectric material is composed principally of two components, one due to ionic conduction, and the other due to dipole rotation. The latter is probably the dominating factor. Polar molecules within the material are continually reoriented with the alternating field, generating heat by friction. This effect in an imperfect capacitor may be attributed to resistance heating in an equivalent resistance placed either series or parallel with a pure capacitance. The alternate equivalent circuits of a charge to be heated dielectrically are shown in Figure 7 A and 7 B with the corresponding vector diagrams in Figures 8 A and 8 B.

Identical expressions for the rate of

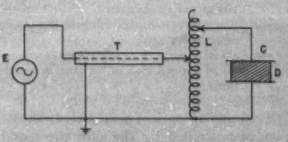


Figure 6. Schematic circuit for dielectric heating showing power source E, interconnecting transmission line T, and tuned load circuit

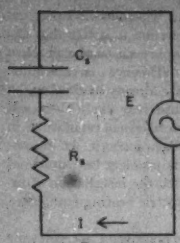


Figure 7(A). Series equivalent circuit for dielectric heating charge

C_s–effective series capacitance, R_s–effective series resistance

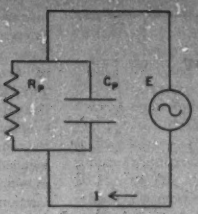
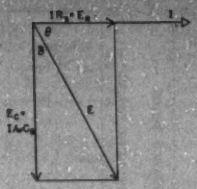


Figure 7(B). Parallel equivalent circuit for dielectric heating charge

C_p–effective parallel capacitance, R_p–effective parallel resistance



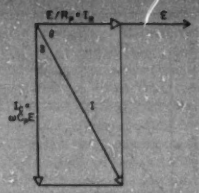


Figure 8(A). Vector diagram for series equivalent circuit

Figure 8(B). Vector diagram for parallel equivalent circuit

dielectric heating may be derived from either Figure 7 *A* or 7 *B* provided it is remembered that

$$C_p = C_s \cos^2 \delta$$

For brevity the heating equation will be derived only for the parallel equivalent circuit. It is first necessary to evaluate the effective parallel resistance R_p which is of course responsible for the heating. From Figure 8 *B* it is apparent that

$$\cot \theta = \tan \delta = \frac{E/R_p}{\omega C_p E} = \frac{1}{\omega C_p R_p} \qquad (8)$$

and that

$$R_p = \frac{1}{\omega C_p \tan \delta}$$

in which $\tan \delta$ is defined as the dissipation factor of the charge being heated. Heating takes place in accordance with equation 1 so that

$$H_p = I_R^2 R_p = E^2/R_p = \omega C_p \tan \delta E^2 \qquad (9)$$

If equation 9 is evaluated for the specific case of heating a charge of uniform thickness between plane parallel electrodes, the result is

$$W = 5/9 k \tan \delta f (E/d)^2 \times 10^{-13} \qquad (10)$$

in which

W = heat generated in watts per cubic centimeter
k = dielectric constant of charge
$\tan \delta$ = dissipation factor of charge
f = frequency of power source in cycles per second
(E/d) = voltage gradient between electrodes in volts per centimeter

It is apparent from equation 10 that the rate of dielectric heating is directly proportional to the loss factor = $k \tan \delta$ of the charge being heated, the frequency of the applied voltage and the voltage gradient between the electrodes. Ideally it should be possible to increase the rate of heating indefinitely merely by increasing the magnitude of any one of these variables. There are in each instance, however, limiting practical considerations.

When the voltage applied to an electrode is increased, a value is reached at which a corona discharge takes place from it, or finally a disruptive arc. This limits the voltage available for dielectric heating to a maximum of approximately 15 kv, with a reliable, reproducible upper limit usually not exceeding 10 kv.

As the frequency of the voltage employed in dielectric heating is increased,[3] thereby making possible higher heating rates, the following difficulties are encountered.

1. Standing waves develop on the electrodes causing nonuniform heating.
2. Value of inductance required for tuning is decreased to an unachievable minimum causing difficulty in tuning.
3. Impedance of load is diminished causing difficulty in maintaining a high voltage gradient.
4. Losses in power oscillator tubes increase very rapidly.

Experience has shown that for large scale industrial dielectric heating the most useful frequency range is from 10 to 30 megacycles.

The remaining variable determining the rate of dielectric heating is the loss factor of the charge. This, however, is not an independent variable since for a given material both dielectric constant k and dissipation factor $\tan \delta$ are a function not only of frequency but of temperature as well. Figure 9[3] shows the variation of loss factor with frequency for materials *A* (cured neoprene with granulated cork as filler) and *B* (granulated cork with a cured organic binder) that are suited to industrial dielectric heating, and for a high frequency insulating material (Mycalex).

Although the changes in loss factor with frequency in Figure 9 are marked, they are not as significant as the changes with temperature indicated in Figures 10 and 11.[4] In Figure 10 are plotted both the dissipation factor $\tan \delta$ (curve *A*) and the power factor $\cos \theta$ (curve *B*) for granulated cork with an organic binder, while being heated from room temperature to approximately 220 degrees Fahrenheit during a curing cycle. It is to be observed that above 200 degrees Fahrenheit this material is very nearly equivalent to a metallic conductor. In Figure 11 curve *A* represents the variation of dielectric constant k with temperature and curve *B* the variation of loss factor $k \tan \delta$ with temperature for the same material. Figure 12[4] provides the corresponding curves of temperature rise and total current to electrodes as a function of time during a curing cycle of this material. During the heating cycle of Figures 11 and 12 the frequency was maintained constant at ten megacycles and the voltage gradient constant at 800 volts per inch. The change in the slope of the temperature versus time curve of Figure 12 must be attributed to variation of loss

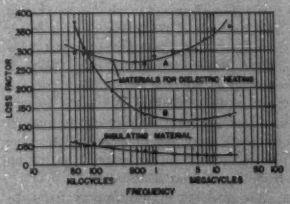


Figure 9. Variation of loss factor ($k \tan \delta$) with frequency

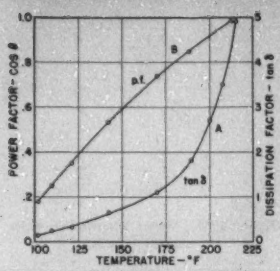


Figure 10. Variation of power factor (cos θ) and dissipation factor (tan δ) with temperature

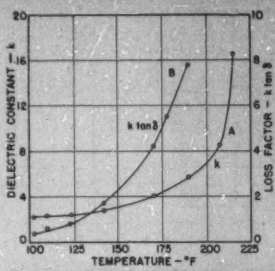


Figure 11. Variation of dielectric constant (k) and loss factor (k tan δ) with temperature

factor with temperature as has been shown to be the case in Figure 11. Therefore, equation 10 for rate of dielectric heating may be used only for qualitative predictions unless knowledge of the variation of the loss factor of the charge with temperature is available.

Great emphasis has been placed upon the fact that heat is generated uniformly throughout a volume in dielectric heating. It should be noted that this does not assure uniformity of temperature throughout the volume unless precautions are taken to avoid heat losses from the surface. In curve *A* of Figure 13 is a plot of temperature through the thickness of a volume at the end of a dielectric heating cycle where no precautions have been taken. A variation of approximately 20 per cent of the maximum temperature is to be noted even in a 6-inch thickness. Curve *B* of Figure 13 shows what may be accomplished by heating the electrodes by auxiliary means during the dielectric heating cycle to avoid heat losses at the surfaces in contact with the electrodes.

Another practical consideration in the use of either dielectric or induction heating oscillator equipment is radiation from it that may cause interference with other services such as radio, police, and military communications, or television. Unshielded heating electrodes and leads definitely radiate a signal as is clear from curve *A* in Figure 14 in which a maximum field strength of 376 microvolts per meter is measured at 1,500 feet from a 1.5-kw output dielectric heating unit. Experience has shown that the radiation pattern from such a unit is normally assymmetrical so that it is necessary to make a complete polar plot in order to determine the possibility of interfering radiation from a particular installation.

The problem of interference from induction and dielectric heating equipment has been recognized already in Canada and the United States. In the United States the Federal Communications Commission (FCC) has issued a definite set of rulings in the matter. The FCC has assigned the following channels to industrial heating 13.66 megacycles ± 0.05 percent, 27.32 megacycles ± 0.5 per cent and 40.98 megacycles ± 0.05 per cent. As an alternative any frequency may be employed provided the oscillator unit and load circuit are sufficiently well shielded that the field strength in any direction at one mile does not exceed ten microvolts per meter. The practicality of shielding is proved in curve *B* of Figure 14 illustrating that a maximum field strength at 1,500 feet of 376 microvolts per meter may be reduced to 42.3 microvolts per meter. At one mile the latter signal could not be detected. For either alternative some remedy must be made if industrial high-frequency heating interferes with other vital services.

Because of the difficulty of stabilizing the frequency of an oscillator and associated load circuit, particularly at high output power, to within ± 0.05 per cent or even ± 0.5 per cent, it is felt that the more practical approach to the problem of avoiding interference is shielding. The FCC rules are already in effect in the United States, and very likely there will eventually be international regulations in the matter. Therefore, it is recommended that this problem be considered in connection with the installation of induction and dielectric heating equipment in Mexico.

The cost of the equipment necessary for dielectric heating and high-frequency induction heating is relatively great. Therefore, it should not be considered in cases where equivalent results are possible by other means of heating. Despite the high capital investment required, however, the advantages of this type of heating are sufficient in many instances so that widespread use is being made of it in the United States at the present time.

In Figures 15*A* and 15*B* are plotted average costs for purchase and maintenance of such equipment. Figure 15*A* shows the initial capital investment required. Curve *A* in Figure 15*B* ,on the other hand, shows the total operating cost per hour of the equipment, which consists of curve *B* for power cost, curve *C* for tube replacement cost, curve *D* for depreciation on investment based on a 5-year life and 4,160 hours operation per year, and curve *E* for miscellaneous maintenance costs.

Applications of Dielectric Heating

From the preceding, it is evident that the outstanding advantages of dielectric heating are

1. Rapid heating cycles; materials conventionally requiring hours are heated in as many minutes.

2. Heat generated uniformly throughout volume, resulting in correspondingly uniform temperature provided certain precautions are taken.

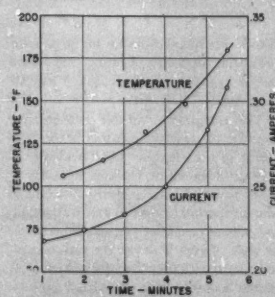


Figure 12. Temperature rise and current to electrodes during dielectric heating cycle

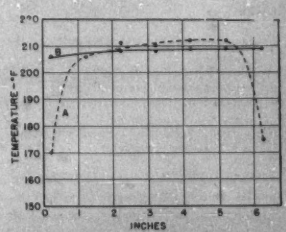


Figure 13. Variation of temperature through the thickness of a charge heated dielectrically

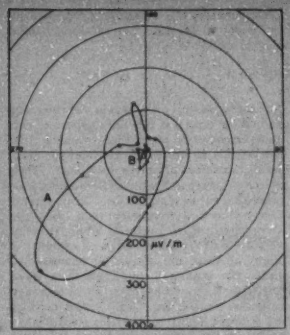


Figure 14. Polar plot of radiation from dielectric heating equipment

A—equipment unshielded, *B*—equipment shielded

3. Precise control of heating possible.

4. Class of materials suited to dielectric heating (electrical and thermal insulators) most unsuited to conventional means of heating.

These advantages have resulted in dielectric heating becoming firmly established in the industries of plastic molding, plywood and furniture bonding, rubber processing, textile, and food.

One of the earliest applications of dielectric heating was in the bonding of plywood, especially that of high density. In order to set the glue line between the plies of a plywood board of appreciable thickness, it was necessary to hold the board between steam-heated press platens for times of the order of hours. With dielectric heating such boards may be bonded in a few minutes. This technique of setting glue or other adhesives has been carried over into the wooden furniture industry. Perhaps the most widespread use of dielectric heating, at least from the point of view of number of operating installations, is in the plastic molding industry. If plastic molding materials such as phenol and urea formaldehyde are preheated by dielectric means to approximately 250 degrees Fahrenheit prior to placing them in the forming molds, great advantages are realized. Not only is the molding time drastically reduced, but lighter molding processes may be employed and the quality of the product is improved. Because of the low thermal conductivity of rubber, the vulcanization of any rubber product other than a thin sheet is a time consuming process between steam-heated press platens. Natural rubber and most synthetic rubbers are heated very readily by dielectric loss so that by this means sections several inches thick are vulcanized in times of the order of a few minutes. Since the heat is generated uniformly throughout the volume of rubber, the vulcanization of cure effected is thereby more uniform and the quality of the product produced improved. The method has been applied to sponge or foam rubber as well as higher density material. So far as the textile industry is concerned, dielectric heating has been applied with very good success to drying and setting the twist in synthetic fibers such as rayon. There are several such large scale units in operation in the United States. In fact dielectric heating is likely the best method available for quickly and completely removing the final traces of water from materials. The drying of the drug penicillin on a mass production basis is a typical example.

Already dielectric heating is employed extensively in the food industry and it is anticipated to play an even more important role in the future. It provides a high speed and continuous method for sterilizing cereals and grains. Equipment is available, operating in the microwave range, that will heat ready to serve in 60 seconds a precooked frozen meal. As well as being convenient this method avoids considerable waste, particularly in railway dining cars and in airplanes.

One industry of importance in Mexico that is not benefited by dielectric heating is glass processing. The dielectric properties of glass, that is, dielectric constant k and dissipation factor $\tan \delta$ are of such an intermediate character that glass has a loss factor $k \tan \delta$ which is neither high enough to make it suited to industrial dielectric heating nor low enough to make a satisfactory high-frequency insulator.

Conclusion

Induction and dielectric heating, particularly the latter, have had a spectacular growth in the United States during the past ten years. At the present time there are approximately 500,000 kw of installed power for this type of heating in the United States, the equipment for which is valued at between $40,000,000 and $50,000,000. The volume of business in supplying this equipment is estimated to have doubled every four years for the past 30 years when induction heating was first used in industry. The annual dollar volume at present is approximately $8,000,000. At the present time the power in use for high frequency heating is far in excess of the power utilized for radio broadcasting in the United States.

Certainly induction and dielectric heating should play an important part in the industrialization of Mexico. The electric power is available, or will be soon, and many of the industries now in existence or planned for Mexico are those that can employ high-frequency heating profitably. Mexico will have the advantage that induction and dielectric heating equipment may be installed initially in new factories as they are built rather than be installed to replace, and therefore make obsolete, conventional heating equipment, as has frequently been the case in the United States. Although the capital investment for induction and dielectric heating equipment will be greater than that required for conventional heating equipment, the use of the former will certainly result in higher rates of production and improved quality with fewer rejects. A high level of production will allow product prices to be set lower than would otherwise be possible, which in turn will tend to improve the standard of living throughout Mexico.

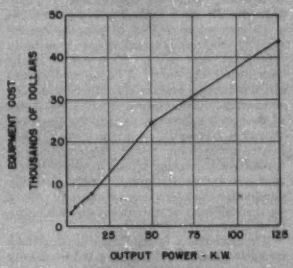


Figure 15(A). Cost of high-frequency heating equipment

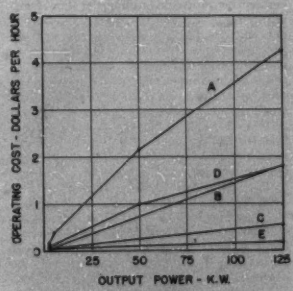


Figure 15(B). Operating cost of high-frequency heating equipment

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1. Radio Frequency Heating, G. H. Brown, C. N. Hoyler, R. A. Bierwirth. D. Van Nostrand Company, Inc., New York, N. Y., 1947, pages 17, 29–30.

2. Electronic Generators Extend Induction Heating Fields, H. C. Humphrey. *Electronics*, (New York, N. Y.), volume 16, 1943, pages 56–59.

3. Role of Frequency in Industrial Dielectric Heating, G. W. Scott, Jr. AIEE Transactions, volume 64, 1945, pages 558–62.

4. Dielectric Heating, The Measurement of Loss Under Rising Temperature, J. B. Whitehead; AIEE Transactions, volume 66, 1947, pages 907–10.

Discussion

H. B. Osborn, Jr. and J. W. Williamson (Ohio Crankshaft Company, Cleveland, Ohio): This paper was presented for the purpose of outlining possible uses for induction and dielectric heating for the benefit of prospective users of this equipment. Unfortunately, however, the paper, as presented, is quite misleading and does not express the thinking and attitude of the manufacturers and users of induction and dielectric heating equipment.

Intended to interest people in the use of induction and dielectric heating, the various equations presented serve to mystify anyone but an electrical engineer. Let us not forget it is but rarely that an electrical engineer is involved in specifying processing equipment in a plant. Data on results and applications would have been more appropriate.

References to depths of penetration according to equations used are not in order in discussing skin effect. Skin effect to the user means the depth of hardness obtained when induction heating is applied for surface hardening. The equations refer to depth of penetration in which most of the electrical energy is concentrated in the case of a bar heated throughout. High frequency energy for surface hardening is used at power densities sufficient to minimize heat flow by conduction and leave the core cold. Only on through heating operations do such equations have any significance.

The skin effect in induction heating does make it possible to surface harden objects with irregular surface contours, such as gears. However, the implication that such a result can be obtained on all gears must be corrected. Due to the rapid heat flow by conduction, notwithstanding the short heating cycles and the pronounced skin effect of the high-frequency currents used, such processing must necessarily be limited to medium pitch and small diameter gears. In order to maintain high-power density per unit of surface area, high-powered high-frequency vacuum tube oscillators are required, the cost of which, to date, is prohibitive. However, in most cases, large gears are generally coarse pitch and 10,000-cycle equipment producing contour is used.

The statement is made that a gear or other metal object which has been raised to red heat in a few seconds can be automatically self-quenched by the action of the cold core. Again, an erroneous implication must be corrected. Obviously, this can only be obtained with materials of high hardenability, for example, high-alloy steels. Regardless of the shallowness of the depth of heating, a carbon steel cannot be hardened in production to complete martensite on the surface by the self-quenching technique. Since the majority of parts induction hardened are made of carbon steel, this point must not be overlooked.

The high cost of high-frequency equipment is generally admitted, but it should not be stated that such equipment should not be considered in cases where equivalent results are possible by other means of heating. Superior quality can be obtained with induction heating; further, there are also many jobs which cannot be done in any way except by induction heating. However, the majority of induction-heating installations are justified on a basis of cost of producing equivalent results due to the automatic features and high production which save on labor, reproducibility of results, and elimination of scrap.

The data shown in Figure 15*A* is incorrect, or at least improperly identified. It is possible that the author meant the cost of vacuum tube oscillator equipment, but of course this is not stated, and by implication the cost figures would apply to all high-frequency equipment. To illustrate, a complete 200-kw 3,000 cycle unit, tooled for processing of a part automatically, costs around $25,000. The same comments may apply to Figure 15*B* covering the operating costs. It is noted that Figures 15*A* and 15*B* are referred to in the section entitled Dielectric Heating. However, in various paragraphs before this reference, the author refers to induction-heating equipment as well, and in the paragraph directly preceding the reference, to the cost of such equipment.

The first subject considered in the paper is radiant heating, which, it is noted, heats only the outside of the piece. The author points out that, in contrast, induction heating is not confined entirely to the surface. If it is desired to take full advantage of this characteristic of induction heating for through heating, the frequency should not be too high. While these principles should be kept in mind, the conclusion that the maximum frequency should not exceed

$$f = 7.9\frac{\rho}{\mu r^2} \times 10^7$$

is not justified. For example, suppose a 2.0 inch diameter carbon steel load is to be heated to an average temperature of 2,100 degrees Fahrenheit. For an allowable 150 degrees Fahrenheit temperature differential, using a frequency of 3,000 cycles, the required heating time is 95 seconds. Other forms of heating would require a much greater heating time. Taking the resistivity of steel as 1.15×10^{-4} ohm centimeter, the frequency given by the above equation is 1,410 cycles. The value of 1.15×10^{-4} ohm centimeter corresponds to a temperature above that at which steel loses its magnetism. If a lower temperature were considered, μ would be much greater than one, and the frequency obtained would be very much less than 1,410 cycles.

Equation 3 correctly gives the rate of heat generation (abwatts/cm³) where H is expressed in oersteds. Converting to the units of equation 5,

$$H = 0.4\pi H_e$$

$$\rho = 10^9 \times \frac{1}{\sigma}$$

$$\sqrt{\rho\mu f} = \frac{1}{\sigma} \times \sqrt{10^9 \sigma \mu f} = \frac{10^9}{2\pi} \times \frac{1}{\sigma d}$$

Therefore, substituting into equation 3,

$$\frac{\text{Abwatts}}{C_m^3} = 10^7 \times \frac{H_e^2}{\sigma d}$$

Consequently, P in equation 5, representing the watts to the load per centimeter axial length, should be given by

$$P = 2\pi r \times \frac{H_e^2}{\sigma d}$$

The source of this error was in the reference quoted for equation 5. The authors of this book erroneously state that in going from just outside to just inside the load the magnetic intensity abruptly doubles. They therefore arrive at a value of P which is four times as great as it should be. Their derivation assumes a frequency such that the wave length of the radiation is negligible in relation to the radius of the piece heated. It might be that the derivation is corrected under the assumed conditions, but these conditions are not representative of ordinary induction heating practice. Frequencies of the order or 30,000 megacycles would be required for the assumed conditions to apply.

In the author's conclusion, he does state that the use of induction and dielectric heating equipment, though expensive, will result in higher rates of production and improved quality with fewer rejects. However, it is felt that such a statement is rather anticlimatic after what has gone before.

G. W. Scott, Jr.: It is the feeling of the author that it is rather presumptious on the part of the discussers to state so arbitrarily their opinion of the purpose of the paper in question. This paper was prepared for an audience of electrical engineers to give sufficient basic information so that an unbiased opinion might be formed of the applicability of induction and dielectric heating. Therefore the use of equations is quite appropriate and in the opinion of the author does much to counteract the influence of many loosely written papers appearing in trade journals prepared largely in an effort to promote the sale of induction and dielectric heating equipment. The term "skin effect" was used in electromagnetic theory long before the induction heating industry developed. Therefore, before an audience of electrical engineers it is considered appropriate to use the term thus.

The additional details provided by the discussers in the matter of surface hardening gears are interesting. In a short paper every point raised cannot be exhaustively treated.

The discussers object to the author's statement that high-frequency equipment should not be considered if equivalent results are possible by other means of heating. They point out that better quality can be achieved by induction heating or jobs done that can be accomplished in no other way.

A little thought indicates that in such cases equivalent results would not be possible by other means. Therefore this criticism appears groundless.

Figures 15*A* and 15*B* are admittedly somewhat ambiguously labeled. However from curve *C* in Figure 15*B* for tube replacement cost, it should be quite apparent to any electrical engineer, as indeed it was to the discussers, that vacuum tube oscillator equipment is implied. Elsewhere in the paper it is clearly stated upon what occasions such equipment must be employed.

The criticism of equation 5 should be passed along to Doctor Brown and the other authors of the book Radio Frequency Heating cited in reference 1.

A Brief Review of Switchgear and Circuit Breaker Practice in the United States

M. H. HOBBS
MEMBER AIEE

Although it obviously is impossible to cover all phases of current switchgear practice in the United States in a single paper except at great length, a general review is given here which will show the trend in several of the more important apparatus lines and their application to various industries. Statements regarding practices and designs have been made, so far as possible, to show those of representative systems and manufacturers. The figures, however, show only equipment produced by the company with which the writer is associated.

General

Primary circuit breakers and switchgear will be divided first into several basic classes, and the equipment applicable to each will be reviewed, covering the various locations, circuits, and industry applications in which it is American practice to use them. These classes are as follows:

1. Low voltage–up to 600 volts alternating current—750 volts direct current
2. Medium voltage–low or medium interrupting capacity 2,300 to 15,000 volts—25,000 to 500,000 kva.
3. Medium voltage–high interrupting capacity 15 kv to 34.5 kv—500,000 to 2,500,000 kva.
4. High voltage–46 kv to 287 kv—500,000 to 10,000,000 kva.

Low-Voltage Circuits

In the low-voltage field, as far as general application is concerned, there has been relatively little change in practice in the last few years. The applications include buildings, lighting, and power circuits of 120/208, 240, and 480 volts, industrial plant utilization circuits, and supply for lower capacity auxiliaries in power stations. Industrial plant and station auxiliary circuits are usually 480 volts, with a few plants retaining their former practice of 240 or occasionally 550 to 600 volts.

Except for certain industrial plant so-called "hazardous" locations where oil circuit breakers are supplied, the practice is uniformly to use air circuit breakers. The most important advances in recent years have been the replacement of semi-elliptical brush main contacts by silver line-to-line solid block type, and the elimination of the open arc by means of multiple-plate arc chutes. Carbon tipped arcing contacts have also been supplanted by arc-resisting metal alloys, such as copper tungsten.

The former and present standard mounting practice has been to mount the circuit breakers on insulating slabs of slate, asbestos, or similar material. The latest designs being brought out have the pole units mounted upon individual moulded bases which incorporate the terminal studs. These poles, two, three, or four as the case may be, together with the frame and mechanism, are then mounted upon a steel base or switchboard panel, providing a stronger assembly of lighter weight. Only the smaller circuit breakers are now operated manually and even these may be electrically operated for remote manual or automatic control. Figure 1 shows a late design circuit breaker.

Greater attention than in the past is being given to selective tripping of the low-voltage circuit breakers, particularly in larger industrial plants and for station auxiliary circuits. Although current transformers and relays are used on some of the heavier main supply circuit breakers and over-current and time-delay tripping characteristics in the majority of the circuit breakers are self-contained, a number of improvements have been made in these devices recently to obtain greater selectivity and reliability, in an effort to provide protection more nearly approaching that of relays with instrument transformers on high-voltage circuits. Of course there are limits as to what may be done in a complicated system.

For the structures in which low-voltage circuit breakers are enclosed, it has become almost universal practice to use metal-enclosed multiple-unit assemblies with circuit breakers which draw out for disconnecting and for maintenance and

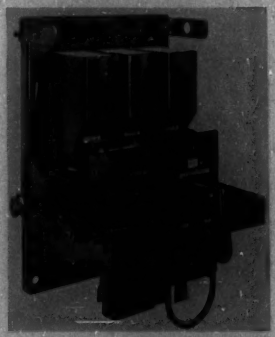

Figure 1. Modern low-voltage circuit breaker of 25,000 amperes interrupting capacity at 600 volts, with steel base suitable for drawout mounting

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M. H. Hobbs is with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

Figure 2. Metal enclosed low-voltage structure for drawout circuit breakers

Incoming circuits with heavy capacity breakers at the left, feeders at the right

inspection accessibility. These structures are factory assembled, tested and shipped in large groups, and the circuit breakers will interrupt their rated current when in their enclosures, due to the arc control obtained by the multiplate chutes mentioned previously. Stress is placed upon accessibility and safety, and interlocks prevent contact with circuit breakers unless they are withdrawn and thus disconnected. The busses are bare and mounted upon moulded insulating supports.

Figure 3. Magnetic blowout air circuit breaker removable element 150,000-kva 5-kv rating with baniers and left hand arc chute removed

Figure 2 shows an assembly of metal-enclosed units.

For 120/208 volts distribution in cities, the use of secondary network systems has become almost universal. The network is fed from transformers through low-voltage circuit breakers called network protectors. These trip automatically on reverse energy, indicating feeder trouble, and reclose if correct voltage conditions exist at the network transformer. In the larger cities the system is all underground with submersible transformers and network protectors located in manholes or vaults.

Medium-Voltage Circuits

In this class are included voltages of 2,300 to 15,000 volts and interrupting ratings of 25,000 to 500,000 kva. The field includes small power stations, municipal plants, steel mills, and other large and small industries, as well as the higher voltage (2,300 volts) auxiliary circuits in heavy power stations.

A material change has taken place in this field which was formerly served completely by small and medium capacity circuit breakers of the oil type. Although many oil circuit breakers are still used and will be for some time to come, the tendency is increasingly toward the magnetic blowout air circuit breaker, a complete line of which is now available with interrupting ratings from 50,000 to 500,000 kva.

The general construction of the oilless

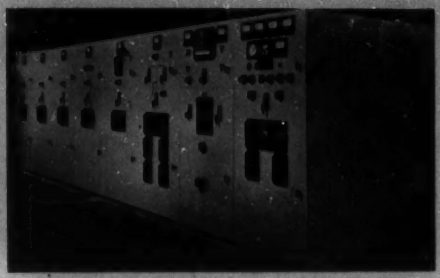

Figure 4. Metal-clad switchgear structure for oil or magnetic blowout air circuit breakers for steel mill application 500,000-kva 7.5-kv rating

circuit breaker for 2.3 to 13.8-kv operation somewhat resembles that of the low-voltage device, with a hinged moving contact arm, primary, secondary, and arcing contacts, and multiple-plate arc chutes to confine and control the arc. This is brought about by the blowout coil through which the current passes, the magnetic force driving the arc into the slotted plates of the arc chute. Copper-tungsten arcing contacts are used and silver-to-silver main contacts. Each circuit breaker is mounted on its own wheeled truck which is withdrawn horizontally from its metal-clad housing, providing convenient disconnection and accessibility for inspection and maintenance. The circuit breakers are all solenoid operated. A typical 5-kv 150,000-kva circuit breaker unit with one barrier and one arc chute removed is shown in Figure 3.

Here again it is the practice to mount the circuit breakers in metal-clad switchgear, either vertical lift or horizontal drawout type. Relays, instruments, and control switches are mounted upon panels which either form the door through which the circuit breaker is removed, or are mounted on the opposite side. In any case, they are usually a part of the metal-clad structure, permitting the control wiring to be entirely self-contained except

Figure 5. Unit substation comprising transformer and outdoor metal-clad switchgear with magnetic air circuit breakers

Figure 6. Indoor power center with primary metal-clad switchgear unit at the left, air insulated transformer, and low-voltage metal-enclosed feeder units at the right

Figure 8. Side view of metal-enclosed cubicle with compressed air circuit breaker 1,500,000-kva 15-kv, showing disconnecting switches at the right and bus above

for the control supply cables. A steel mill installation with 500,000-kva circuit breakers for 6,900-volt service is shown in Figure 4.

Usually a single bus or sections of bus with ties between prove adequate. However, where a double bus is required, the metal-clad units are placed back to back and the circuit breakers are withdrawn from opposite sides of the structure. Shipping units comprise several units connected together and completely assembled.

Figure 7. Standardized unit substation with frame structure for high-voltage switching equipment, transformer and metal-clad switchgear for distribution feeders

As is also the case with the low-voltage equipment, both indoor and outdoor metal-clad switchgear units are available and many of both types have been installed in recent years. Their application is particularly suited to unit substations in the primary distribution systems, and a typical unit comprises a transformer fed from a high-voltage transmission line through a circuit breaker, fuse, or switch; and a metal-clad structure with a transformer circuit breaker and several feeders fed from a single bus. Voltage regulation is obtained usually by under-load tap changers on the transformer, rather than by individual feeder regulators. An example of an outdoor unit substation is pictured in Figure 5, and an indoor structure with dry-type transformer, a primary circuit breaker, and 440-volt feeder circuit breakers is shown in Figure 6.

For larger substations, additional feeders may be added, and also additional transformers, although the tendency is to use a larger number of unit substations of lower rating with fewer feeders in each. The primary network system of distribution has been used to a certain extent, al-

Figure 9. Installation of metal-tank oil circuit breakers for 230-kv 3,500,000-kva

thorgh the radial system seems more suitable in the majority of areas.

For the high-voltage circuits feeding the transformer, many systems use a steel frame structure upon which are mounted the busses, disconnecting switches and their connections, with the circuit breakers, transformers, instrument transformers, and lightning arresters located below. The most recent development in these structures is the trend toward standardization, which enables the building up of the larger assemblies from smaller basic units as extension of the station is required (Figure 7).

Although this standardization has not progressed to where manufacturers are justified in carrying basic units in stock, nor are the majority of operating companies following the plan, there is a definite tendency toward greater uniformity than in the past.

Medium-Voltage High Capacity

This is the class of circuit breakers and switchgear which has been used for the generator voltage circuits in large power stations and the more important substations in large cities. The voltage in the great percentage of stations is 13.8 kv, with interrupting ratings of 1,000,000, 1,500,000, and 2,500,000 kva.

Here again the tendency is toward oil-less circuit breakers, although in these ratings the type is compressed air rather than magnetic blowout. Many oil circuit breakers are still used, but installations of these are in general limited to additions to existing structures. Although the compressed air circuit breaker was introduced by American manufacturers only in 1940, the few intervening years have seen it become accepted almost universally for heavy power-station use.

The circuit breaker operating mechanism is also pneumatic, as compared to the electric solenoid mechanism used for oil circuit breakers, and its valves are interlocked to insure that adequate air pressure is available for the blast before tripping can take place.

Figure 11. Outdoor disconnecting switch with horn gap 46-kv rating, with high-pressure rotating blade contact

Switchgear structures for these circuit breakers are metal enclosed almost exclusively, but they differ from the lower capacity smaller structures in that the circuit-breaker unit is not the drawout type. Because of the circuit-breaker size, isolation is by means of disconnecting switches manually operated. All six switch poles on the two sides of the circuit breaker operate together from a single mechanism, which is interlocked to insure that the switches may be operated only when the circuit breaker is open. The doors giving access to the live circuit-breaker parts are also interlocked with the switches to permit their being opened only after the switches are open, and to insure that they shall be closed before the switches may be closed. Figure 8 is a cross section of a cubicle unit with a compressed air circuit breaker.

Another difference in construction between these heavy metal enclosures, called "cubicles," and the lower-capacity metal-clad switch gear is that metal barriers are used between phases to give complete phase isolation and insure against phase-to-phase faults in the structure. This is carried throughout the cubicle, from the circuit breaker to the outgoing or incoming circuit, and is usually followed on connection or bus runs to transformers or generators.

Instrument transformers are incorporated in the cubicles, the current transformers being the ring or bushing type, and mounted upon the disconnecting switch bushings. Potential transformers are the drawout type, preventing access to the fuses unless disconnected.

Although a very considerable amount of generator voltage switchgear is being installed in the United States, there is now somewhat of a trend toward its elimination except where required by distribution feeders connected directly to the generating station bus. The growing practice is for generating stations to feed bulk power over high-voltage transmission circuits and bus the circuits only at the high-voltage level. Thus, each generator and transformer, and perhaps even the line, become a unit with ties only on the high-voltage side of the transformer. As previously stated, where power is distributed at generator voltage from the bus, as is the case in many stations, switchgear at that voltage is obviously required.

A popular circuit arrangement, for medium and even high capacity stations, is the "synchronizing bus," whereby the

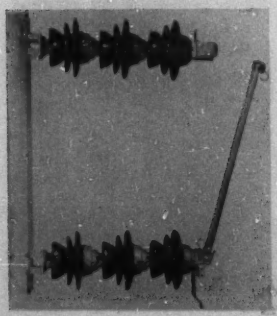

Figure 10 (left). Outdoor expulsion fuse 110 kv shown in partially open position

Tube swings down after blowing of fuse

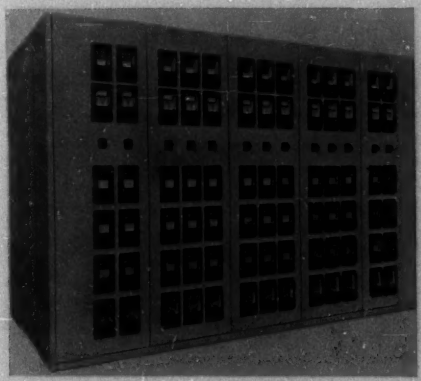

Figure 12. Protective relay side of duplex switchboard

generators are tied together only through reactors to a bus which normally transfers relatively small amounts of power. This scheme reduces circuit breaker interrupting capacity materially, but power transfer and voltage regulation must be watched carefully. Many large industrial plants and steel mills have installed this bus arrangement. Metal enclosed cubicles and bus runs work out well with the synchronizing bus with the reactors as well included in the complete metal-enclosed structure.

There is a growing tendency to apply indoor compressed-air circuit breakers for 23-kv and 33-kv service, using indoor cubicles, and at least one outdoor-cubicle installation has been made. However, the majority of applications at these voltages, as well as many at 13.8 kv are served by outdoor oil circuit breakers of the frame mounting dead-tank oil type.

High Voltage 46 to 287 Kv

High voltage might be considered to include ratings lower than 46 kv, but the line here has been drawn at that voltage because the type of circuit breaker is almost exclusively outdoor at that voltage and above. Here the outdoor oil circuit breaker of the metal-tank type has been holding its own for many years against the inroads of other types, such as compressed air and "oil poor."

There are a limited number of installations of the compressed air and "oil poor" types in different parts of the country, some being experimental, but the great majority of circuit breakers being installed are the metal tank oil type. Improved interrupters have permitted the reduction in oil content by approximately 50 per cent in the past few years. Figure 9 is an installation view of 230 kv metal tank circuit breakers.

The demand for increased interrupting ratings has been much greater for high voltage than in the lower classes. The former American Standards Associations circuit-breaker tables list standard ratings only as high as 3,500,000 kva, and this only at the higher voltages. The tables as now revised list 3,500,000 kva at 69 kv, and 5,000,000 at 138 kv, 161 kv, and 230 kv. In addition, the bureau of reclamation has purchased for Grand Coulee power station, circuit breakers of 10,000,000 kva at 230 kv from two American manufacturers.

The interrupting time on high-voltage circuit breakers has been brought down in recent years until the standard for 230 and 161 kv circuit breakers is three cycles, measured from the time of energizing the trip coil. The 138-kv circuit breakers are five cycles, except the 5,000,000-kva rating which is three cycles. The 115-kv ratings are all five cycles and, for all voltages below this, the standard is eight cycles.

High-speed reclosing is becoming more common for the higher voltages, and 20 cycles has been made the industry standard for this feature. In order to obtain such speed, it is usual to supply pneumatic mechanisms. The electric solenoid, because of its greater simplicity and reduced maintenance, is still used widely on lower voltage circuit breakers and those not requiring rapid reclosing.

Ring-type current transformers for relaying and for instruments are mounted upon the bushings just below the mounting flanges, and in many cases potential taps on the bushings are provided for instrument and relay potential in place of instrument transformers.

Fuses

A large percentage of the fuses now used for higher voltage circuits are either the expulsion or the current limiting type. The latter has been applied for all voltages up to 15 kv and has been developed for special applications such as motor starting in which time-delay features are included, and for potential transformers where the current is unusually low.

The expulsion type has been developed both for indoor and outdoor use, and for outdoor applications ratings as high as 138 kv have been installed. After the fuse has blown, the tube swings down from its normal position to give a blown fuse indication. Such a fuse (115 kv) is shown in Figure 10.

Disconnecting Switches

The principal developments in indoor switches recently have been the standardization of insulator post dimensions, insulation levels, and strength classes, and the use of silver line contacts in place of flat copper. Modern switches are shown in the cubicle in Figure 8. In outdoor switches there is a growing tendency to use a high-pressure rotating-blade type of switch as pictured in Figure 11. The blade rotates completely out of contact before there is any movement about the blade hinge to the open position, resulting in very easy operation. The blade is counterbalanced by a spring in the casting which completely encloses the mechanism, and operation takes place from rotation of the intermediate insulator post. This type of switch may be mounted in any desired position.

Switchboards

All switchboards and control desks are now built of steel or other metal, providing stronger and lighter construction. Although many control desks are used, the trend is toward the duplex vertical panel arrangement, with spacing between front and rear panels wide enough for a working aisle. The instruments and control switches are mounted on the front or operating side, and the relays on the rear. It is usual to mount and wire all devices upon the panels in the factory and ship completely assembled. Figure 12 shows such a board.

Instruments, relays, and meters are quite generally the semiflush type which gives a very pleasing appearance, and relays are now furnished with built-in disconnecting and testing devices as standard.

Conclusion

Three general aims are evident in both the manufacture and application of switchgear and circuit breakers in the United States. First, a simplification in bus and circuit arrangement to provide the least complicated switching scheme to simplify operation and maintenance. Second, the purchase and installation of switchgear equipment as completely factory assembled as possible, to require the minimum field installation labor. Third, the elimination of oil in indoor circuit breakers, and greater use of magnetic blowout and compressed air types.

Discussion

R. L. Webb (Consolidated Edison Company of New York, Inc., New York, N. Y.): The author has done an excellent job of condensing a mass of information in his paper on Switchgear and Circuit Breaker Practice in the United States. He has covered the subject remarkably well in the short space allotted.

I would like to say a few words about some of our recent practices and experiences with switchgear in New York City.

In new generating station installations, auxiliary power will be supplied directly from the bus, or generator terminals, at 13.8 kv through small unit-substations, which consist of an air-cooled transformer and associated low-voltage air circuit breakers rated 600 volts. In one station, these substations are each rated 750 kva 3-phase 120/208 or 440 volts, and are located at strategic points around the station to be near the load they supply. The 440-volt stations for the larger motors are located only a few feet from the motors to avoid the cost of cables or busses. With this arrange-

ment, 440-volt motors and low-voltage switchgear were found to give the most economical design.

As Hobbs has said, use is being made of oilless circuit breakers wherever possible in the higher voltage systems. We have recently installed six distribution substations in the Queens district, each of which transforms from 27 kv to 4 kv. They supply four or six distribution feeders from each substation. The substations each consist of a 5,000-kva tap-changing-under-load transformer and a switchgear unit having automatic magnetic-air circuit breakers. The feeder circuit breakers have three single pole units mounted on a common framework, thereby forcing the outage of only one phase for a sustained line-to-ground fault on a 4-kv distribution feeder. The substations are completely automatic and are unattended.

Our experience with this type of switchgear and with air-blast circuit breakers at medium voltage—high capacity has been quite good. There have been no electrical failures and only minor mechanical and pneumatic difficulties. Our feelings toward oilless gear is perhaps best expressed by saying that we have recently placed orders for over $2,000,000 worth of switchgear for use at 4 kv, 13.8 kv, 27.6 kv, 69 kv and 138 kv; and all of it, with the exception of about $400,000 of 138-kv circuit breakers, will be magnetic air for 4 kv and air-blast at 13.8 kv, 27.6 kv and 69 kv.

We are now in the process of installing a 138-kv double circuit system around New York with direct connections, through transformers, to three existing generating stations and with provision for a fourth generating station connection, in the future. Since all 138-kv circuits in New York City are underground and elevated structures are undesirable, the exposed 138-kv circuits and busses at the stations and at two connected area substations have been designed for minimum height. The busses are made of $4^1/_2$-inch aluminum tubing and are supported on top of 138-kv insulators mounted on steel pipe posts. The structure presents a pleasing appearance with a minimum of structural steel supporting members in use.

S. E. Schultz (Bonneville Power Administration, Portland, Oreg.): Hobbs' paper presents a splendid summary, brief as it is, of the developments in circuit breaker practices. I am very happy to observe that it is now possible for the manufacturers to supply circuit breakers with a rupturing capacity on the order of 10,000,000 kva. This is a distinct advance over the previous circuit breakers rated at 3,500,000 kva. With circuit breakers of this larger rupturing capacity it is possible to achieve the maximum economy from the transmission systems that would have been handicapped if compelled to operate with smaller capacity circuit breakers. The development of the 10,000,000-kva circuit breaker is evidence of the benefits that can be derived by the cooperation of the manufacturer and the system operator, with each having a sympathetic understanding of the other's problems.

I think that it is timely to suggest that the manufacturers consider the building of a low-cost load break disconnecting switch for voltages as high as 230 kv. The need for such a switch on the Bonneville Power Administration transmission system is quite pressing, so much so that active steps, including the testing of such devices, have been undertaken with certain manufacturers.

H. J. Lingal (Westinghouse Electric Corporation, East Pittsburgh, Pa): The paper by Hobbs was solicited by the AIEE switchgear committee for the purpose of making information available to our Mexican associates on current practice in switchgear construction in the United States, and to indicate for their guidance the probable trend of switchgear design and construction as we foresee the future.

During the period when engineers from Mexico were studying designs and accumulating manufacturing information to begin production of electrical switchgear by Industria Electrica de Mexico, S.A., I had the opportunity to work very closely with them and urge the use of methods and designs developed in the United States without overrating on the expectation that lighter duty or tolerance of lower grade performance would permit use of lower rated apparatus.

At this time I should like to repeat a word of caution, not only to the Industria engineers but to all our Mexican friends, that there is danger in overapplying or in overrating apparatus on the assumption that money would be saved by taking some additional operating risks.

The application of switchgear beyond its proved rating may be completely satisfactory operating practice for the immediate time, but as the industry develops and its requirements become more severe, as they surely will in the future of Mexican power system operation, vast benefits will be derived from following what may appear now to be too rigid application practice. Almost all standards in the United States are higher and not lower than in the past, and operators have always been grateful for any safety factor which has been present in the equipment. The apparatus used by the author to illustrate the type of switchgear now operating successfully in the United States required many years to develop and prove, and he has chosen it carefully and well. We wish to commend the author on his selections and recommend them for the consideration of the growing electrical industry in Mexico.

M. H. Hobbs. The discussions of my paper by Webb, Schultz, and Lingal are very much appreciated. Although no controversial points have been raised, the following comments on these discussions seem pertinent.

As Schultz has stated, the development of the 10,000,000-kva circuit breaker is an outstanding example of co-operative effort between the manufacturers and large forward-looking users' organizations, such as that of Schultz and of the Bureau of Reclamation. It is only by this means that the development would have been possible.

He mentions the desirability of a low-cost load break disconnecting switch for voltages as high as 230 kv. It is understood that Schultz has reference to switches which are suitable not only to break load but to be used as fault interrupters for limited kilovolt-amperes in connection with isolated loads.

Although the need for such devices is apparent, their low cost and simplicity depend upon the minimum requirements being given. Where higher interrupting requirements, electrical closing, and automatic reclosing are necessary, the device again approaches a full scale circuit breaker, and the cost rises accordingly. As has been stated, several manufacturers are at present working on the problem.

Webb has given us a very complete picture of the practice in switchgear and circuit breakers followed by his company in New York. Of particular note is the trend toward oilless circuit breakers, culminating in the purchase of the 69-kv compressed air units, the first commercial installation of such circuit breakers for this voltage in the United States. This selection was made only after exhaustive studies of the offerings of American manufacturers.

The installation of 138-kv bus, and equipment referred to in his last paragraph, is outstanding and will undoubtedly appeal to many operating engineers.

The Grand Coulee Power Development

S. M. DENTON
ASSOCIATE AIEE

THE Bureau of Reclamation announced in 1903 that the soil in the Columbia Basin was suitable for irrigation and in 1911 further reported the soil to be high in fertility. The only source of irrigation, the Columbia River, was about 600 feet below the irrigable land, and there the matter rested until 1922 when General Goethals of Panama Canal fame stated that a plan for a dam, power plant, and pumping plant was sound. Support for the plan grew, and it was favorably commented upon by Secretary of Commerce Hoover and by President Coolidge.

On July 27, 1933, the Federal Emergency Administration of Public Works provided an initial allotment of $15,000,000 to begin construction, and an additional $20,000,000 was made available on June 21, 1935, under the Works Relief Program to continue construction. The Interior Department Appropriation Act of June 1936 provided $20,750,000 for the project. Subsequent appropriations allowed completion of the dam and an orderly power plant installation as well as investigations and surveys for the irrigation development.

The irrigation development will bring water to one of the largest bodies of undeveloped land in the United States. The proposed power installation when completed will exceed any power plant output of today.

Planning the Development

The Grand Coulee Dam and power plant, which is a part of the Columbia Basin Project, is located in the state of Washington about 90 miles west and north of Spokane and about 240 miles east of Seattle. The dam and power plant are in the Columbia River immediately downstream from the point where a great cleft was carved in the river canyon south wall centuries ago by the antediluvian waters. This cleft is known as the Grand Coulee.

The geologic history of the project area is interesting, but perhaps only a brief summary of the later developments that were responsible for the natural conditions to which the project was adapted are pertinent here.

During the period of the Pleistocene era when ice sheets spread over North America, an ice mass blocked the deep canyon of the Columbia River at this point. The river formed a huge lake and flowed over the rim of the canyon and started cutting a new channel at right angles to its former course. Thus, the Columbia River gouged a new canyon, the Grand Coulee, 50 miles long, from 500 to 1,000 feet deep, and from two to five miles wide. After thousands of years the ice obstruction melted away and the river returned to its original course, leaving the cleft cut by the overflow, high and dry.

The river canyon at this point provides an excellent power site. A natural site for a huge regulatory reservoir for irrigation supply is made by the addition of two dams in the Grand Coulee. The reservoir is about 27 miles long covering an area of approximately 28,000 acres with one dam about 90 feet high approximately two miles from the main dam, and one about 100 feet high near the town of Coulee City. It will store about 700,000 acre-feet of water at an elevation that will enable irrigation of 1,000,000 acres of arable lands by gravity flow. These lands are covered by a deep and rich blanket of soil that will be highly productive when irrigated.

The water supply for the regulatory reservoir will be taken from the Columbia River. The Grand Coulee dam will raise the water over 300 feet, but the additional lift of 270 to 365 feet of the huge flow required will necessitate pumping on an unprecedented scale. A pumping plant is being built adjacent to the upstream face of the dam along the left bank of the reservoir. The pumping plant building contemplates an ultimate installation of 12 pumps. Six pumping units are now being manufactured and are rated 1,600 cubic feet per second with 295-foot lift. Each pumping unit comprises a vertical-shaft centrifugal-pump direct-connected to a 65,000-horsepower motor which will operate at 200 revolutions per minute.

The cost of power from the Grand Coulee development will compare favorably with the cost at the most economical sites in North America. The low cost of power at this site makes the pumping of water for irrigation purposes economically feasible.

The Columbia River above Grand Coulee dam has a drainage area of approximately 79,000 square miles, and it has an average annual discharge of 80,000,000 acre-feet. The average river flow is just over 100,000 cubic feet per second at the Grand Coulee dam site.

Franklin D. Roosevelt Lake, which is the reservoir formed by the dam has a storage capacity of about 9,500,000 acre-feet of which 5,165,000 acre-feet is live storage. With a drawdown of 80 feet, a firm continuous power output of 925,000 kw can be maintained. This will be improved with the addition of upsteam storage and regulation such as the Hungry Horse development which is now under way.

Grand Coulee dam, which is approximately 4,200 feet long, is provided with 11 spillway gates each 28 feet high by 135 feet long, and has 60 $8^1/_2$-foot-diameter outlet conduits. Designs for flood flow were based upon a discharge of 1,000,000 cubic feet per second over the 1,650-foot-wide spillway. A power plant is located on each end of the dam. Pumping power

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S. M. Denton is with the United States Bureau of Reclamation, Denver, Colo.

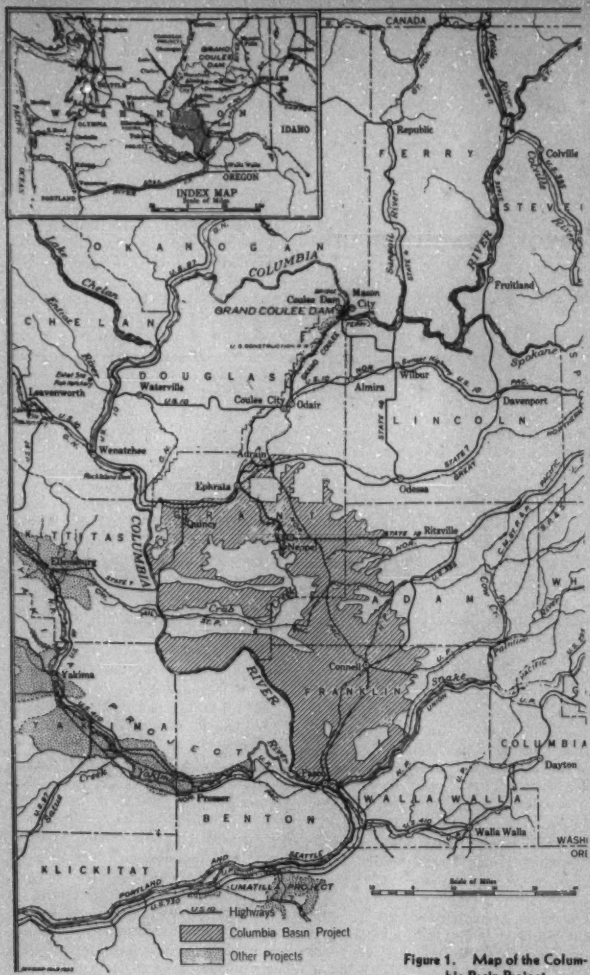


Figure 1. Map of the Columbia Basin Project

requirements can be met largely by utilizing off-peak power.

Basic Arrangement of Power Plant

The large flood flow and the unprecedented height of the overflow spillway dictated that the spillway be located in the midsection of the dam. The length of the spillway section allows insufficient space at either end of the dam for the entire power development. The power plant was accordingly divided into two sections, with one powerhouse on each side of the spillway section. The amount of power to be developed and the limited space available indicated that units of the largest practicable size should be used. One limiting feature was found to be the physical size of the turbine runner. It was deemed important that the turbine runner be of one piece construction in order to avoid a bolted connection and the hydraulic disturbances which such a connection would entail.

The powerhouses are located on the downstream toe of the dam which is the most economic, simple, and desirable arrangement. The best operating conditions and hydraulic efficiency resulted from this arrangement permitting the shortest possible penstocks. The fluctuation of more than 80 feet in the water surface of the tailrace complicated the designs for the powerhouse structure. The low setting of the turbines and the large tail water fluctuation necessitated heavy transverse walls to carry the water pressures. Generator and gallery floors were designed as girders transmitting the heavy water-pressure loads into transverse walls between units. These considerations resulted in the location of the generators 53 feet above the center line of the turbine scrollcase.

The power plant location on the toe of the dam provided a deck between the upstream generator room wall and the dam where the transformers were located. The generator voltage busses, the 230- and 115-kv lightning arresters, and a take-off structure for the transformer circuits are also located between the generator room and the dam. This arrangement eliminates a more expensive transformer supporting structure above the draft tubes, as is more commonly used. Towers for supporting the 230-kv transformer circuits are located on the roof of the powerhouse.

The main generator room is equipped with two 350-ton cranes for use in erecting and servicing the generating units. The heaviest parts require both cranes working in tandem with a lifting beam. The service bay is at the entrance end of the generator room and provides space and facilities for erecting the turbines, generators, and transformers, and also unloading and setting down space.

The governors, gauge and thermometer boards, unit control and power boards, and the entrances to the turbine pits are located in the governor gallery. This area has sufficient indication and control facilities to permit emergency operation of the units. Longitudinal galleries through the powerhouse substructure serve for piping control cables, generator voltage busses, and other equipment.

The control room for each powerhouse is located at the abutment end of the powerhouse. A terminal board room is located immediately below the control room and a cable spreading room is provided below the terminal board room. The cable spreading and cable terminal rooms are usually combined in smaller

power plants, but separate rooms are advantageous in plants of this magnitude.

The Grand Coulee left power plant building houses nine main generating units and two station-service generating units with all necessary controls, protective devices, handling and service equipment. Provision is also made for a future third station-service generating unit.

Generating Units

Turbines

Nine hydraulic turbines each rated 150,000-horsepower at 330-foot head and 120 revolutions per minute are installed in the left powerhouse. The right powerhouse will house nine turbines of similar rating. At 263-foot head, each turbine will develop 90,000 horsepower. The effective head under which the turbines will operate may vary from 263 to 355 feet.

The turbine runner is of the Francis type and is of 1-piece cast-steel construction. The turbine shaft is 44 inches in diameter and is provided with a 6-inch-diameter hole bored axially throughout its entire length to permit inspection of the metal in the interior of the shaft.

The turbine guide bearing is of the babbitt-lined, oil-lubricated type. Oil is circulated through the bearings by means of either of two motor-driven oil pumps. One oil pump is driven by a 3-phase a-c motor connected to the station-service system. The other pump is a standby pump driven by a d-c motor from the station battery and is arranged to start upon failure of the oil pressure.

The speed ring and scrollcase are of cast steel construction and divided into sections small enough to meet shipping limitations. Removable and renewable wearing rings are provided for close-running clearances between the runners and the stationary parts of the turbine. The servomotors have a combined capacity to supply the maximum force necessary to operate the turbine gates with a minimum oil pressure of 250 pounds per square inch and will move the turbine gates a full opening or closing stroke within a 4-second interval.

Many unusual problems confronted the engineers in connection with the design and installation of these turbines due to the fact that they are the largest hydraulic prime movers in existence. The treatment of a few of these unusual problems is described herewith.

Each turbine is provided with a double gland expansion joint to permit relative movement vertically as well as horizontally between the power plant and the dam to accommodate expected vertical movement at the toe of the dam due to the great pressures in this area with the reservoir filled. This joint has proved very satisfactory.

The size of the Grand Coulee turbines was determined by the limits of foundry practice and shipping clearances. The turbine runner measures 16 feet 8 inches in diameter, is 6 feet 8 inches in height, and weighs over 125,000 pounds.

The center line of the turbine distributor was set unusually low to accommodate a high specific speed turbine designed to operate at 120-foot head during initial operation and later to operate satisfactorily at the ultimate head of 330 feet. The low head arrangement was not used, however.

Extensive tests were made on the effect of size and shape of groove to receive round rubber gaskets for sealing the bolted flanged joints. These tests showed the best results were obtained when the area of the groove equaled the area of the rubber gasket. For all practical purposes, the rubber may be considered as incompressible.

Field tests show an over-all efficiency of the unit including generator losses, of 90 per cent. These turbines develop 165,000 horsepower at 330-foot head while discharging approximately 5,000 cubic feet of water per second. Two station-service turbines are also installed in the left powerhouse. Each of these turbines drives a 12,500-kva, 80-per cent power-factor generator and is rated 14,000 horsepower at 400 revolutions per minute.

Each turbine is supplied through a concrete and steel trashrack structure at the upstream face of the dam. A vertical gate operated by an oil-actuated hydraulic hoist is mounted at the upper end of each penstock and controls the water flow into the penstock. Each unit is supplied by an 18-foot welded-steel penstock with a reducing bend at the turbine connection.

Governors

The governors for the 150,000-horsepower turbines are of the oil-pressure, relay-valve actuator type with electrically driven speed-responsive elements. The speed-responsive elements are actuated by permanent-magnet generators connected to the upper ends of the generator shafts. The governors control the wicket gates by means of oil pressure applied to the servomotors and are adjustable to provide variable rates of operation from completely opening from closed position or closing from fully open position in four seconds minimum up to 12 seconds maximum. The speed responsive elements are sensitive to 0.01 per cent variations in turbine speed. The governors may be operated manually at the governor or

Figure 2. View of Grand Coulee dam, powerhouses, pumping plant, and area in right background in which the irrigation main canal passes to the Grand Coulee irrigation reservoir

Figure 3. Grand Coulee dam, powerhouses, and pumping plant structure at far end of the dam

Switchyards in right background

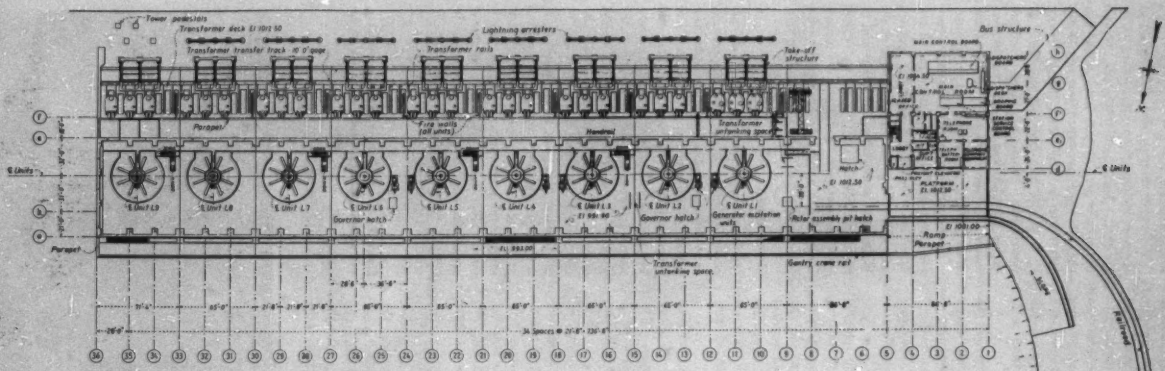


Figure 4. Grand Coulee power plant, left powerhouse

Plan showing generator room, service bay, and control room

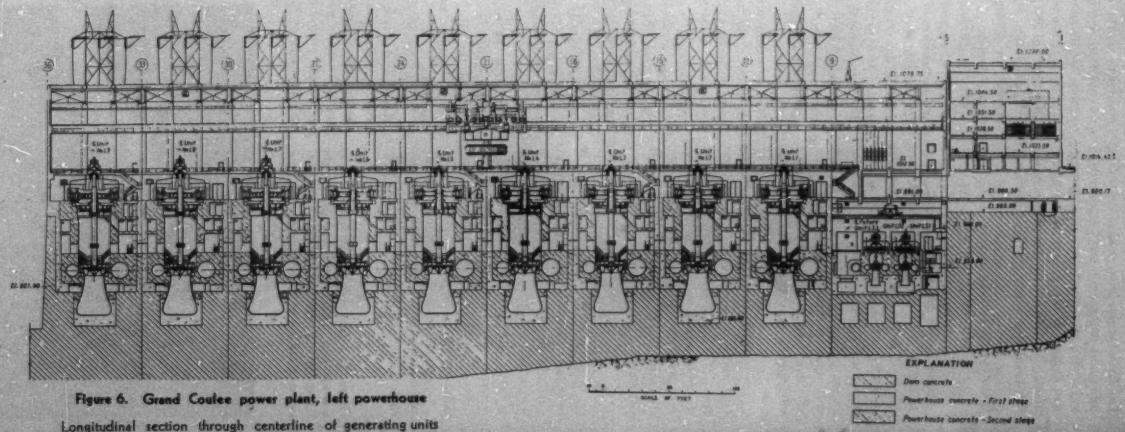


Figure 6. Grand Coulee power plant, left powerhouse

Longitudinal section through centerline of generating units

electrically by remote control from the main control board and include the following features:

1. A gate-limit control device.

2. A speed-level controlling-device with selsyn transmitter and receiver with micrometer adjustment.

3. Manual and automatic starting and shutdown.

4. A device for controlling the speed droop of the turbine. The amount of speed droop is adjustable from 0 to 5 per cent.

5. Speed indicators driven by a magneto-type generator located in the governor-drive-generator housing and geared to the governor-generator shaft.

6. Position indicators for indicating the position of the gate limit device and of the turbine gates.

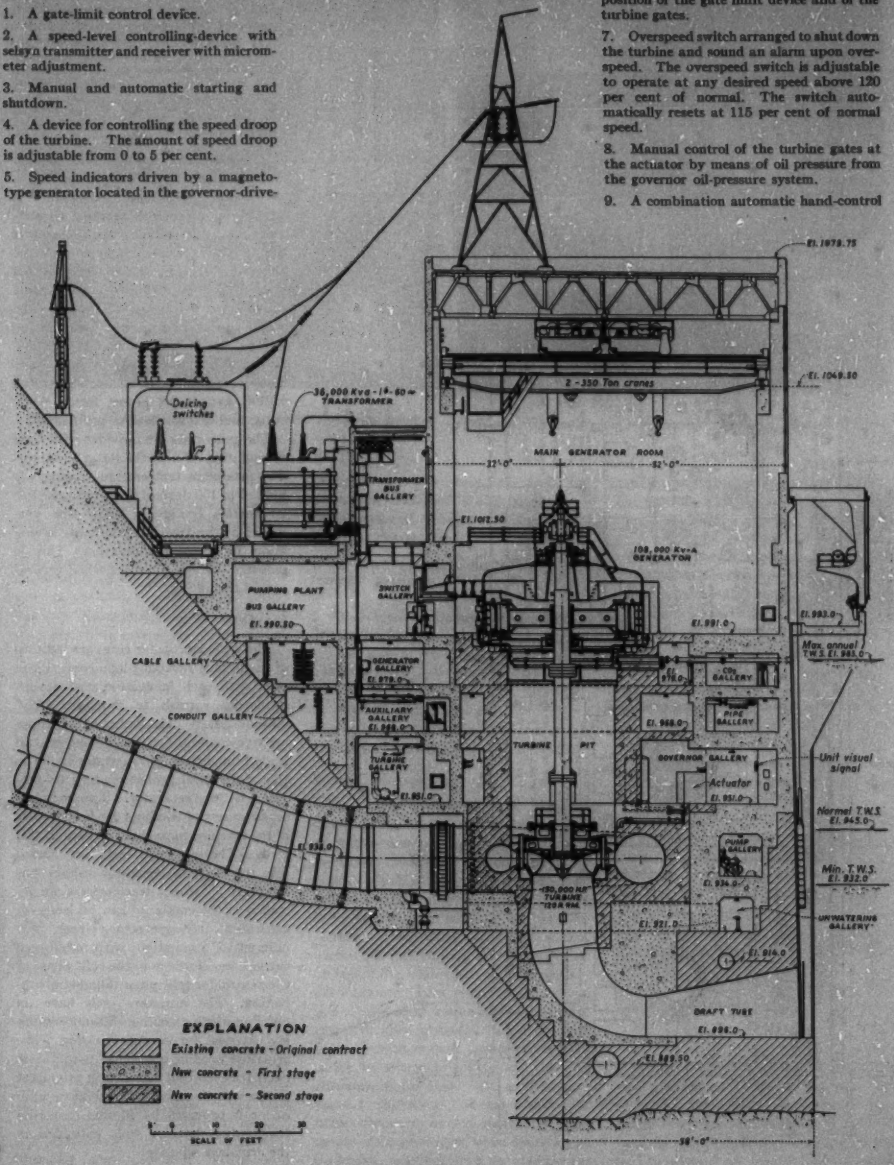
7. Overspeed switch arranged to shut down the turbine and sound an alarm upon overspeed. The overspeed switch is adjustable to operate at any desired speed above 120 per cent of normal. The switch automatically resets at 115 per cent of normal speed.

8. Manual control of the turbine gates at the actuator by means of oil pressure from the governor oil-pressure system.

9. A combination automatic hand-control

Figure 5. Grand Coulee power plant, left powerhouse

Typical section through generating unit



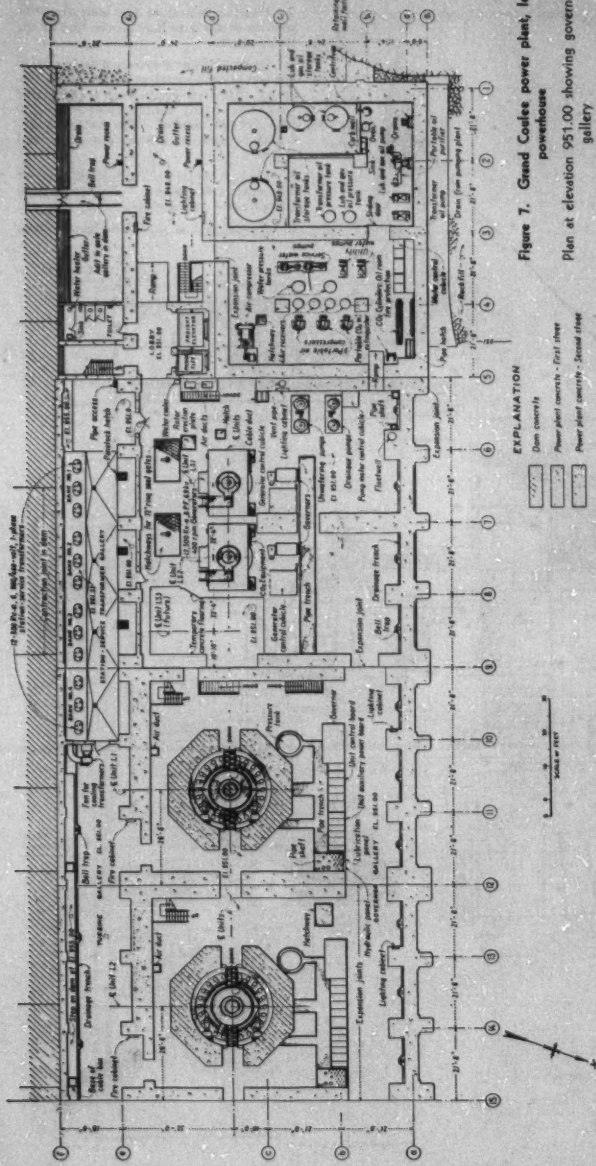


Figure 7. Grand Coulee power plant, left powerhouse

Plan at elevation 951.00 showing governor gallery

and hand-operated air valve for controlling the operation of the generator brakes. The arrangement is such that the brakes cannot be applied automatically until the turbine gates are closed and the speed of the unit is less than 30 revolutions per minute. Automatic application of the brakes is intermittent with periods of time adjustable, until the speed has decreased to a predetermined level. The brakes are applied constantly thereafter until the unit is at rest.

Generators

The main and station-service generators are of the vertical type, totally enclosed and water-cooled. Generation is at 3-phase 60-cycle alternating current. Both main and station-service generators are of the conventional type with the thrust and upper-guide bearings located above the rotor and with a lower-guide bearing located below the rotor. The main generators are each rated 108,000 kva, unity power factor, 13,800-volts, and 120 revolutions per minute. The station-service generators are each rated 12,500 kva at 0.8 power factor, 6,900 volts, and 400 revolutions per minute.

The generators are provided with class *B* insulation for both the stator and rotor. The maximum temperature, however, is limited to 100 degrees centigrade which is 20 degrees centigrade below that permitted by the American Standards Association for class *B* insulation. This is industry practice for water-wheel generators of this rating.

Stators

The generator stator cores are built up of high-grade nonaging-laminated silicon steel with each lamination coated with insulating varnish to minimize eddy-current losses. The armature windings are wye-connected with main and neutral leads brought out of the stator frame so that differential protection may be provided for the generator. Two complete separate sets of windings are brought out of the machine in six main and six neutral leads. The six main leads are bussed into one 3-phase generator voltage bus structure. Current transformers are installed in the combined neutral leads for generator and generator-voltage bus-differential protection with additional current transformers in the split phase of the neutral for split-phase differential protection. The armature coils have an outer protective coating of semiconducting material for corona shielding. The armature winding is designed to minimize circulating current losses with attendant unnecessary heating. Temperature indication is provided by 10-ohm resistance temperature-detector coils imbedded in the armature winding.

Figure 8. Generator room looking toward equipment entrance

Figure 9. Generator room with one unit completed, others in progress

Rotors

The generator-rotor rims are laminated and constructed of steel plates bolted together to form a solid structure. The rotor spider is of cast steel and of 1-piece construction except for rotor arm extensions on six of the arms. These arms, three on diametrically opposite sides of the rotor, have extensions which are removable to provide shipping clearances. The rotor hub was shrunk on the generator shaft by heating the rotor hub and cooling the generator shaft. The pole pieces are of high-grade magnetic steel in the form of thin-punched laminations and are dove-tailed into the rotor rim laminations and held in place by means of tapered keys. The generator rotors are provided with amortisseur windings. The generators have a moment of inertia (WR^2), of about 175,000,000 which is higher than normal for stability purposes. The WR^2 of the generator plus the WR^2 of the turbine produces a regulating constant of 17,130,000. The regulating constant is a product of the revolutions per minute squared and the WR^2 divided by the horsepower rating of the turbine.

Electrical Characteristics

The remoteness of the Grand Coulee plant and the lack of system development in this region at the time the original generators were purchased necessitated considerable study to determine generator characteristics which would be suitable for any reasonable transmission requirement. It was assumed that long distance transmission would be involved with associated stability problems. The following characteristics were selected:

1. A rated current value of direct-axis transient reactance not exceeding 30 per cent.
2. Short-circuit ratio not less than 1.66 as determined by test.
3. The ratio of the calculated value of the quadrature-axis subtransient reactance to the calculated value of the direct-axis subtransient reactance not to exceed 1.2.

The characteristics have proved very satisfactory with the system as developed.

Excitation

Six of the generators have separate motor-driven exciter sets to provide full excitation at zero speed for use in starting the 65,000-horsepower synchronous-pump motors which will be used in connection with the irrigation project. The remaining generators are provided with direct-connected main exciters, and direct-connected self-excited pilot exciters mounted on top of the main exciters. The motor-driven exciters have synchronous motors. A d-c, compound-wound, self-excited pilot exciter is also connected to the common motor-generator shaft and provides excitation for the main exciter and the synchronous motor field. The exciters have class *A* insulation. Exciter capacity of not less than 110 per cent of actual requirements of the generator, when operating at its rating, was specified to provide for some variations from unity power factor. The exciters have operated continuously at considerably in excess of this output when the main generators are operated below unity power factor lagging and at considerable overload. In order to increase system stability, the exciter speed of response was required to be not less than 1.5. The speed of response for a normal machine is 0.5.

The motors of the motor-driven exciter sets receive their power supply from the station-service system. In order to assure sufficient torque to prevent loss of excitation due to momentary dips in voltage on the station power system, a pull-out torque of 250 per cent of rated torque was required in the motors.

Each main generator is equipped with an automatic voltage regulator including a voltage-sensitive master element, main-exciter field rheostat, pilot-exciter field rheostat, and voltage-adjusting rheostat. The voltage regulators are equipped with high-speed contactors for shunting in and out blocks of resistance during transient conditions. Each generator is provided with an exciter field breaker but generator field breakers were omitted.

Bearings

A thrust bearing located above the rotor was specified to facilitate bearing maintenance. The loading on the main generator bearing is approximately 2,000,000 pounds. Each generator is equipped with a Kingsbury-type thrust bearing with

Figure 10. 150,000-horsepower turbine scroll case nearing completion

Note many sections required in order to meet shipping clearances

babbitt on the stationary surfaces. Each of the bearing shoes is adjustable and is provided with a strain gauge for use in adjusting the bearings. The thrust bearings are provided with temperature detectors imbedded in the bearing metal for indicating the temperature of the bearings.

The thrust and upper-guide bearings of each generator operate in a common oil bath. Each lower-guide bearing operates in a separate oil bath. All bearings are of the self-oiling type requiring no external means for forcing oil between the bearing surfaces. All the oil baths require cooling and are provided with internal water-cooled heat exchangers.

Figure 11. 150,000-horsepower turbine runner enroute to powerhouse

Cooling

Each Grand Coulee generator is equipped with an enclosed cooling system with heat exchangers located inside the metal housing of the generator for removing the heat resulting from generator losses. The metal housing is sufficiently airtight to permit the use of carbon-dioxide gas fire extinguishing equipment. This equipment releases carbon-dioxide gas into the generator upon operation of the generator differential relays or whenever temperatures within the housing reach a predetermined level. The coolers are located around the stator frame and have sufficient cooling capacity to maintain the temperature of the air entering the generator at 40 degrees centigrade or less with one cooler out of service and the generator delivering rated output.

Brakes

Brakes are provided on the Grand Coulee generators of sufficient capacity to bring the generator and turbine rotating parts to a stop from one-half normal operating speed within $7^1/_2$ minutes. The brakes also serve as hydraulic jacks for lifting the entire generator and turbine rotating parts a sufficient distance to provide for removal or adjustment of the thrust bearing. Oil pressure for actuating the hydraulic jacks is provided by a motor driven high pressure oil pump.

Transformers

Transformers step up the voltage to 115 kv or 230 kv for transmission purposes. Seven banks of transformers are rated 13.6 to 230 kv, one bank is rated 13.6 to 115 kv, and one bank has three voltages, 13.6, 115, and 230 kv. The transformer banks are of the same rating as the generators and consist of single-phase, water-cooled, nitrogen-gas, cushion-type units. The impedance of the transformers is approximately 11 per cent. Some of the transformers are of the straight water-cooled type while others are of the forced-oil water-cooled type. The latter type represents the later trend in design of large power transformers.

Control

The main unit control features include the control, indication, instrumentation, protection, and annunciation at the unit control boards and at the main control boards. The unit control boards are located in the governor gallery adjacent to each turbine governor. The main control boards are located in the main control room. The main and unit control boards are operated as shown in the following.

Upon receipt of the signal from the operator in the main control room to start a unit, the operator in the governor gallery starts the unit auxiliaries and then starts the main unit and brings it up to

Figure 12. 108,000-kva generator rotor requiring the lifting capacity of two 350-ton cranes

normal speed and voltage. The operator in the governor gallery then signals the main-control-room operator who places the voltage regulator in service, synchronizes the unit with the system, and adjusts the load and voltage level as desired. The main control system includes the following.

Hydraulic Governor Control

Governor oil pressure is normally maintained at all times whether the unit is running or not by means of the motor driven oil pumps in the actuator cabinet. All control for starting and stopping the turbine, operating air brakes, controlling the speed, load limit, and speed droop settings are provided on the actuator cabinet located adjacent to the unit control board. A d-c solenoid is provided on the governor for automatically shutting the unit down by protective relays and by an emergency shutdown pushbutton on the main control board. The main control board has facilities for control of the turbine speed and turbine speed droop by means of selsyn devices, control of the load-limit device by means of a control switch, and for emergency shutdown of the unit. Indication of the position of the turbine gates and the load-limit device is provided on the main control board by means of selsyn devices.

Voltage Regulators and Excitation Control

Control of the excitation includes adjustment of the regulated generator-voltage level and placing the voltage regulator in and out of service. Instruments for reading pilot-exciter amperes and volts, generator-field volts, and main-exciter amperes are mounted on the unit control board. The main-exciter voltage is controlled by means of a motor-operated rheostat, with control switches at both the main and unit control boards. Means is provided at the main control board only for placing the voltage regulator in or out of service.

Equipment Protection

Relays of the differential type, with necessary auxiliary-tripping relays, furnish generator and transformer protection. On occurrence of a fault in the generator or connecting bus within the differential zone, the generator-differential relays operate and trip the high-voltage circuit breakers connecting the unit to the system and also trip the main-exciter-field circuit breakers, shut down the unit, and initiate the discharge of carbon dioxide gas into the generator housing. The operation of the transformer-differential relays performs the same functions as the generator-differential relays with the exception of the release of carbon dioxide gas.

A generator overspeed switch is arranged to close the turbine gates and operate the annunciator in case of overspeed. The outgoing transmission lines are protected by means of directional-distance relays and directional-ground relays, together with carrier-current type relays.

Load and Frequency Control

The load and frequency control consists of the following operating features which may be chosen by means of a selector switch.

1. Maintain system frequency and time error within the narrowest possible limits while using one or more generating units for this purpose or while regulating cooperatively in parallel with other existing power systems.
2. Operate about a scheduled output while contributing a predetermined adjustable amount of regulating assistance to the system.
3. Maintain a fixed output at any desired unit load.
4. Load division between units when operating in parallel on the same bus.
5. Permit parallel operation with other plants on the interconnected system, contributing an adjustable amount of regulating assistance to the system while operating about a predetermined tie-line load schedule.
6. Time error indication for each separate frequency controlled system.

Switching

The unit system is employed at the Grand Coulee power plant with the generators connected directly to the trans-

Figure 13 (left). 108,000-kva generator rotor under construction

Note spider-arm extensions required for shipping clearances

Figure 14. Generator thrust bearing runner and one bearing shoe

Bearing runner is over eight feet in diameter and carries more than 1,000 tons at maximum load

Figure 15. Single-phase unit of 108,000-kva bank of transformers

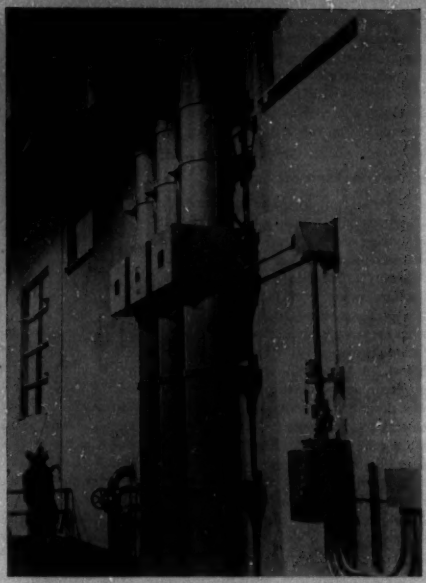


Figure 16. Generator-voltage bus structure of isolated phase construction with each conductor in a separate housing

formers without an intervening generator-voltage circuit breaker except for unit *L-1* which is connected to a 3-winding transformer bank through a generator voltage circuit breaker. The generator-transformer connection is made by means of an air-insulated, isolated-phase bus structure. An air space is provided between the metal enclosures practically eliminating the possibility of phase-to-phase faults. The generator neutrals are connected to ground through a potential transformer arranged to remove a generating unit from service and open the exciter field circuit breaker if a fault to ground develops. This equipment is to be replaced with a single phase distribution transformer with its primary connected between the generator neutral and ground. A resistor is connected across the secondary winding of the transformer. The capacity of the distribution transformer and resistor is each approximately equal to the capacitive kilovolt-amperes of the generator and its generator-voltage connections. The loading of the secondary of the distribution transformer prevents the occurrence of high-transient voltages caused by arcing faults or switching which might occur with the potential transformer grounding. The harmonics present in potential indication are also dampened making a lower setting of the ground relay possible. This and other advantages of the distribution transformer and resistor over the poten-

Figure 17. Governor gallery showing governors, unit control boards, and unit power boards

Equipment between columns controls one generating unit

Figure 18. Main control boards located in control room of left powerhouse

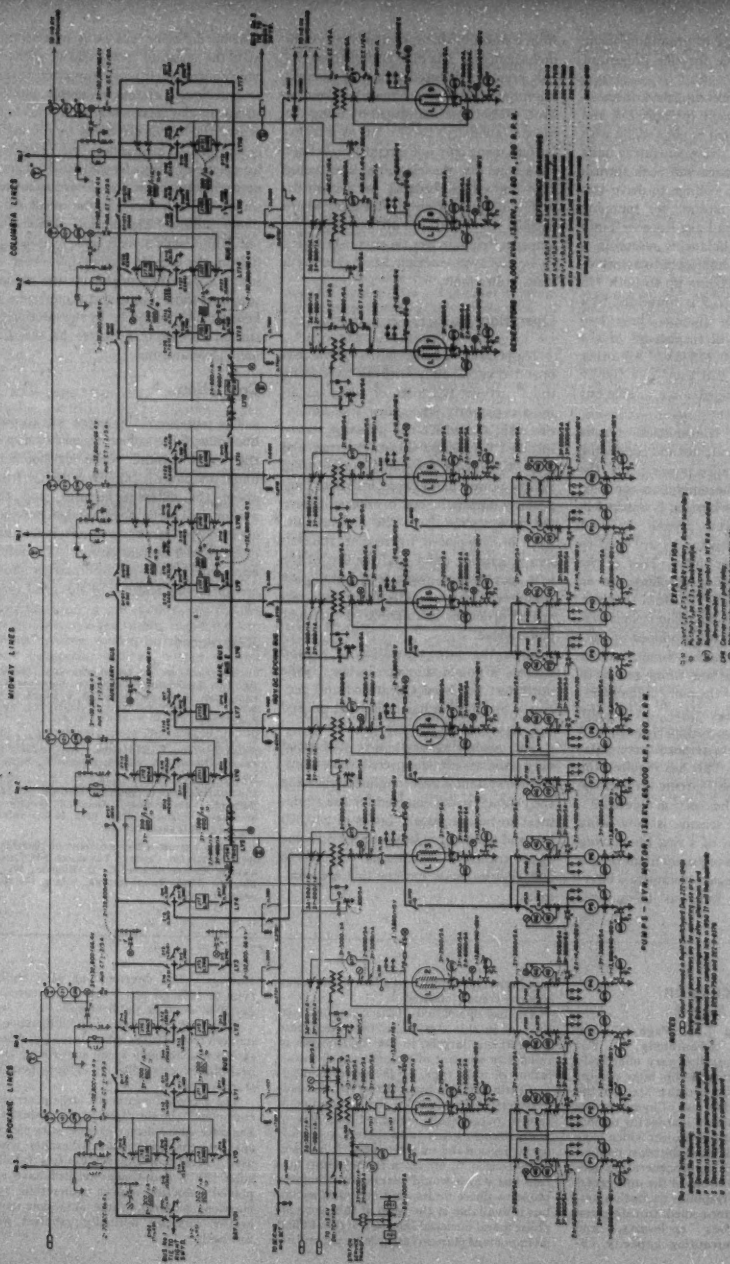


Figure 19. Grand Coulee power plant

Left power plant 230-kv switchyard and pumping plant. Single line diagram

tial transformer type of grounding made the change desirable for this important station.

All switching is accomplished at transmission voltage except for unit *L-1* and the switching required in connection with irrigation pumping. Six generating units in the left powerhouse will each furnish power at generator voltage to drive two 65,000-horsepower motors, for pumping irrigation water from Lake Roosevelt into the Grand Coulee balancing reservoir.

Generator *L-1* is connected to a 3-winding transformer stepping up to both 115 and 230 kv. Unit *L-9* is connected to a 115-kv transformer bank. These two generating units will furnish all of the energy required at 115 kv. All other transmission is at 230 kv. The 230-kv switchyard is arranged for a double bus system.

As the 230-kv transmission system grows with additional lines and generating units at Grand Coulee power plant, the switching problem becomes more complex. The present 230-kv bussing is unusual in that an interleaved system is used whereby the line reactances are used to limit circuit breaker duty. Half of the generators and half of the lines are on each of two busses. The lines tie these busses together and maintain system stability. This also has introduced operating problems due to the circuit breaker shortage during the war which results in each line of some of the pairs of lines having but one circuit breaker. When any line is out of service, system stability and switching limitations require three separate bus sections. This has required the installation of a loop circuit around the yard to tie the end bus section together.

The interleaved scheme is now being abandoned in favor of a ring bus having five sections. This requires circuit breakers of 10,000-megavolt-ampere interrupting capacity, but has advantages from a stability standpoint during system disturbances. The high cost of these circuit breakers dictated that only a minimum quantity of new circuit breakers be used. This is accomplished by using the second (auxiliary) 230-kv bus as a transfer bus, using only one circuit breaker for the second line of each pair of lines, using only one circuit breaker per generator, rebuilding present circuit breakers where feasible to 7,500 or 10,000 megavolt-amperes, tripping bus-tie circuit breakers to isolate a bus section when clearing a transformer fault.

Operation

The Grand Coulee power plant has operated successfully with the generating units carrying full load or overload almost constantly since their installation. Six main Grand Coulee generators were installed before and during the war, and two 75,000-kva generating units from Shasta power plant were installed temporarily during the war in order to relieve the power shortage. The Shasta units have been replaced by regular 108,000-kva generating units, and the last of the left powerhouse-generator installation completed this year. Six additional units are on order for installation in the right powerhouse.

Since the first generation at Grand Coulee, a power shortage in the Pacific northwest has been threatened and generator installation has been hard pressed to keep pace with the power demand.

The generating units have performed very satisfactorily, connected to the transmission system of very long lines indicating the characteristics selected for the equipment to be adequate. Loads of 240,000 kva and above have been carried on some of the transmission lines and 180,000- to 200,000-kva loads for one line are common.

Some difficulty has been experienced with the separate motor-driven exciters which obtain their power supply from the station-service system. Trouble experienced by the station-service system has caused the loss of excitation on the main generators. This has now been corrected by the use of a transfer switch which connects the exciter driving motor to another source automatically in the event of a power failure. The station-service system is also made more reliable by isolating it from the outside system. In order to insure a more dependable station service supply, a third source of station service is being made available by connecting a station service transformer to the *L-1* main generator bus.

Conclusion

The purpose of this paper was to outline some of the salient design features of the Grand Coulee power development under one cover. Much has been written about the different features, and the author wishes to acknowledge previous authors who have covered certain features of the power system design. The references contain further information about certain design features of this great project.

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Discussion

E. F. Bossuyt and J. C. Page (General Electric Company, Schenectady, N. Y.): In this paper Denton has very effectively presented the features of this huge project which make it a noteworthy step in the generation and efficient use of hydroelectric power. It is particularly interesting to note the many features of this project which have either followed or dictated the most modern practices utilized in hydrogenerating stations. We refer to the use of the unit generation system, high-response excitation system, forced oil water-cooled transformers, isolated phase bus enclosures, and 10,000,000-kva interrupting capacity circuit breakers, to mention a few of the many modern trends followed.

The use of isolated phase metal enclosed bus, as in this station, is becoming more general, due largely to the high degree of reliability which it offers and ease of installation of this type of factory-assembled, generating-station bus. This type of bus is also very desirable from the standpoint of safety to operating personnel and ease of maintenance, being immediately accessible for inspection and maintenance. It has long been desired to obtain a factory-built station bus which would practically eliminate phase-to-phase faults, and now that such a bus is available in the form of this isolated-phase metal-enclosed bus, its use should find an important place in future large generating stations. The development of a similar type of isolated phase bus is presented in detail in another publication.[1]

A further indication of the adherence to modern trends is the use of forced oil water-cooled transformers which are proving their desirability especially at hydrogenerating stations.

The accepted exciter for large generators continues to be the direct connect edexciter. Separate motor-driven exciters are generally spares to the direct connected exciter, where used. Most motor-driven exciters installed since the Grand Coulee project have employed induction motors rather than synchronous motors. These induction motor exciter sets are normally supplied with flywheels.

The necessity and ultimate development of circuit breakers capable of much higher interrupting capabilities than those previously available was emphasized and achieved as a result of this large generating station. The increase in circuit breaker interrupting ratings to 10,000,000 kva was a great stride in the industry, which will undoubtedly influence the design of future large systems, and is a development which will henceforth be associated with the Grand Coulee project.

The following of modern trends and initiating of new methods of handling such large quantities of power have made this a unique modern power project.

REFERENCE

1. A NEW ISOLATED PHASE METAL-ENCLOSED BUS DESIGNED FOR HIGH CAPACITY GENERATING STATIONS, B. W. WYMAN, R. B. SHORES. AIEE TRANSACTIONS, volume 67, 1948, pages 699-704.

S. E. Schultz (Bonneville Power Administration, Portland, Oreg.): Denton's paper presents an excellent description of the Grand Coulee powerhouse and sets forth very effectively the basic factors of historical and physical significance in connection with this tremendous development.

The Columbia Basin Project, which includes the Grand Coulee Dam and powerhouse, is so vast in scope and extent that it is difficult to fully appreciate its magnitude in terms of measurable standards. It is difficult, for example, to comprehend, on a purely statistical basis, such terms as 1,000,000 acres of new arable lands, 108,000 kw hydroelectric generators, 10,000,000-kva circuit breakers, and a powerhouse, when fully developed, with an output of over 2,000,000 kw measured in capability rather than rating. 1,000,000 acres would almost cover the state of Delaware, and it is quite evident that the Bureau of Reclamation is not only making history but also remaking geography in this major undertaking. 108,000 kw represents a fair-sized powerhouse. Yet, this is the capacity of only one generator, and there will be 18 such generators at Grand Coulee Dam upon completion of the powerhouse, now scheduled for December 1951. Actually, the generators now in service at Coulee have been producing at the rate of approximately 130,000 kw. In terms of available energy during an average water year, the Grand Coulee powerhouse could provide for the needs of New York City. To my knowledge, the largest circuit breakers built today carry a rating of 3,500,000 kva; yet 10,000,000-kva circuit breakers are required for Grand Coulee.

Additional statistics almost without end could be used to show the tremendous scope of the Columbia Basin project. Great as this development may be physically, there can be no question that its social implications are even greater. The effect of bringing into production this vast area of fabulously fertile farm land will substantially enhance the agricultural economy of the northwest and the nation. From the standpoint of power, the impact of Grand Coulee has already been realized in the tremendous development of industry in the northwest, and with it the material benefits to the nation as well as to the region. Largely because of this great power development along with Bonneville Dam, approximately 50 per cent of the aluminum of the country is now being produced in the northwest, whereas only eight years ago none was being produced. Other industries, including those related to chemical, agricultural, and lumber development, are finding new uses for power on a vast scale. The results are evident in the unprecedented growth of the Pacific northwest.

The Bonneville Power Administration is the marketing and transmission agency for the power output of Grand Coulee Dam, as it is for the power output of the Bonneville Dam which was built and is operated by the United States Army Engineers. These two projects, together with the Bonneville Power Administration system, serve as the federal grid in the Pacific northwest. Other federal projects will follow as the needs of the region grow. The present capacity of the grid is approximately 1,600,000 kw, of which approximately 1,100,000 kw is presently available from Grand Coulee Dam. It is expected that the capacity of this federal system will grow to approximately 3,600,000 kw in 1954, and perhaps as high as 10,000,000 kw in 1960. The technological and economic problems associated with this development will be formidable, but there is little question that these problems can be successfully solved, as evidenced by the progress made to date.

S. M. Denton: The author is indebted to Bossuyt, Page and Schultz, for their valuable additions to the interest of the paper. Agreement by Bossuyt and Page that this great project has followed and, in some instances, led the way in the most modern practices utilized in hydrogenerating stations is gratifying. Deviations from accepted modern practices were generally prompted by problems peculiar to this particular project. Direct-connected exciters are in use or proposed for all generators which will not be used for pumping. Six generators are equipped with separate motor driven exciter sets which were dictated by starting requirements of the 65,000-horsepower motors. In operation, two 65,000-horsepower motors are to be connected to one generating unit at standstill and brought to rated speed by the synchronous starting method. Synchronous motors were specified for the generator exciter sets in order to obtain high pullout torque which can be obtained at reasonable cost in synchronous motors. Induction motors will drive the exciter sets for the 65,000-horsepower pump motors. Here, however, loss of motor excitation is not considered as serious as with one of the large generating units. Induction motors have simpler controls and are preferable when the application is suitable.

Schultz' discussion added a very worthwhile picture demonstrating the magnitude of this development and brought out some of the social aspects and responsibilities in a project such as this. It greatly increases the value of the paper from an over-all viewpoint and adds a new field of interest.

Power System Overvoltages Produced by Faults and Switching Operations

AIEE GENERAL SYSTEMS SUBCOMMITTEE

POWER system overvoltages may result from lightning, switching, faults, loss of load, and other causes. An understanding of the phenomena associated with all causes of overvoltages is necessary in order to control the magnitude and to co-ordinate the protection and insulation levels of the system. Lightning may be considered the chief source of high overvoltages. Much attention and study has been and continues to be given to all lightning phenomena and means of protection. Next and of increasing relative importance are the overvoltages produced by faults and switching surges. Considerable study has been given this subject during the past 25 years and it is opportune to review the literature on power system overvoltages produced by faults and switching surges. This is the purpose of this report. Conclusions are drawn which indicate the progress and understanding to date and which may serve also as a guide to future investigations.

The results obtained from field tests on power systems are reviewed as well as the phenomena that control cumulative oscillations and restrikes. In addition, the results obtained by the miniature system method of analysis are given which show the influence of grounding, arrester location, and circuit breaker interrupter restriking.

Summary of Knowledge on Switching Surges

Early systematic investigation of system overvoltages was delayed because of difficulties encountered in field measurements and in calculations. Initial efforts to measure system overvoltages were directed principally to the securing of information on lightning which, incidentally, gave valuable data on switching surges. Not until 1924 through the use of "Lichtenberg" figures as a method of voltage measurement on power systems were there much field data obtained on switching surges, and not until after the introduction of miniature system methods in 1937 was the determination of system overvoltages produced by faults and switching operations generally facilitated.

Measurement of Switching Surges on Power Systems

The spark gap was the earliest device used for the measurement of system overvoltage. However, the first really practical devices were the klydonograph and the surge voltage recorder.[1-4] In these devices, a voltage proportional to that to be measured on the transmission system is impressed on a photographic film by a set of point-to-plane electrodes. The record appears on the film in the form of "Lichtenberg" figures, which can be calibrated to give data as to polarity, magnitude, and duration of the surge. The addition of a clock mechanism to drive the photographic film adapted the instrument for measuring overvoltages during normal system operation. From the "Lichtenberg" figure measurements, a great amount of data on switching surges has been obtained. The accuracy of the measurements has been limited and the results therefore are of somewhat questionable value.

Progress in lightning studies led to the application of a more accurate instrument—the cathode-ray oscillograph.[5-9] To obtain information on the magnitude of surges during normal operation of a system, special electronic relays were used to make the oscillograph inoperative except for surges in excess of a predetermined magnitude. In the laboratory the device produces very satisfactory records of the magnitude-time characteristics of system overvoltages. In the field the cathode-ray oscillograph is cumbersome and expensive when used as a surge recorder for obtaining overvoltages during normal operation of a power system. However, it is more applicable to set system tests.

Mention should be made also of other forms of surge recording devices that have received limited use. The magnetic oscillograph is a very satisfactory device for recording overvoltages in prearranged tests, provided the natural frequencies of the system are sufficiently low with respect to the natural period of the oscillograph element.[10] Vacuum tubes have been used also which permit current flow when a given voltage is exceeded, this current being used to actuate an operation counter or other recording device.[10] A few vacuum tube devices have been used, principally for the lower voltage systems that are below the range for successful operation in obtaining "Lichtenberg" figures. More recently the magnetic link recorder,[11,12] which has been used successfully for measuring the crest current in a lightning surge, has been adapted to record the maximum voltage of a switching surge.

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Personnel of the AIEE General Systems Subcommittee: S. B. Crary, *chairman*, General Electric Company, Schenectady, N. Y.; O. A. Demuth, Bonneville Power Administration, Portland, Oreg.; R. G. Hooke, Public Service Electric and Gas Company, Newark, N. J.; E. K. Huntington, Rochester Gas and Electric Corporation, Rochester, N. Y.; A. A. Kroneberg, Southern California Edison Company, Los Angeles, Cal.; L. B. LeVesconte, Sargent and Lundy, Chicago, Ill.; E. L. McClure, Wisconsin Electric Power Company, Milwaukee, Wis.; L. M. Robertson, Public Service Company of Colorado, Denver, Colo.; H. M. Trueblood, Bell Telephone Laboratories, New York, N. Y.; and R. L. Witzke, Westinghouse Electric Corporation, East Pittsburgh, Pa.

The major work of writing the first draft of this report was done by L. D. Barter and the late R. D. Evans both of the Westinghouse Electric Corporation. The final draft was written by I. B. Johnson of the General Electric Company.

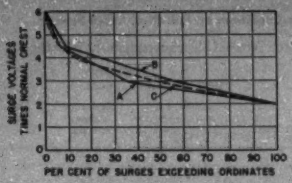


Figure 1. Distribution of surge voltages caused by switching and faults

A. Switching surges
B. Surges from faults
C. Switching surges

A and B—18 systems, 1925–1926
C—14 systems, 1926–1930

(*Figure* 1, *reference* 19)

Table I.* Summary of Surges in One Year at One Point on Each of 14 Cable Systems

Cause	Number of Surges: Times Normal Voltage 1.1 to 1.9	2.0 to 2.9	3.0 to 3.9	4.0 to 4.9	Unidirectional 1.1 to 2.5	2.6 and over	Oscillatory 1.1 to 2.5	2.6 and over	Total	Per Cent Total
Closing a switch at a station where a klydonograph was located	1,050	92		1	1,120	23			1,143	42.4
Opening a switch at a station where a klydonograph was located	340	25		1	360	6			366	13.6
Switching at other points of the system	117	13	5		120	15			135	5.0
Cable failures, automatic interruptions	300	49	13	5	150		185	32	367	13.6
Unknown	660	12	5	3	645	23	12		680	25.4
Total	2,467	191	23	10	2,395	67	197	32	2,691	
Per cent total	91.6	7.1	0.9	0.4	89.0	2.5	7.3	1.2		100

* From Table V, reference 14.

Overvoltages Obtained During Field Tests on an Overhead Power System

An early extensive report of switching surges on transmission systems was published in 1925 in which the data were obtained by means of flashovers of needle gaps on photographic film and the klydonograph.[18] In 1927 and subsequent years, other extensive investigations were published.[8,9,14–17] The results of these representative field studies indicated by the reference numbers were presented mostly in tabular form. Later investigators prepared summaries of the published data, and these together with reproductions of some of the results from the representative studies are shown in Figures 1 to 9. It is to be noted that all curves except Figures 5 and 6 have times normal crest line-to-neutral voltage as ordinate. It is to be noted also that the percentage distribution curves, Figures 1 to 4, do not have a common overvoltage reference base at 100 per cent on the abscissa. For example, in Figure 1, the percentage distribution starts with overvoltages of two times normal at 100 per cent, disregarding all the surges of a magnitude less than twice normal. This was done in preparing these summary curves in order to give a more suitable scale for plotting the results of the surge studies. In Figure 2 the base voltage is two and one-half times normal and in Figures 3 and 4 one times normal.

The investigations which covered a large number of systems and several years of operation provided data on the magnitude of surges, identification with type and location of switching operation, and point of measurement. The data also included similar information for surges resulting from faults and the consequent fault-clearing operations. For comparison a summary of lightning surge data as to magnitude and frequency of occurrence is shown in Figure 3. Data on the magnitude of switching surges appearing at various points on a system as a result of de-energizing a line are shown in Figures 5 and 6. All investigators report that a de-energizing operation on a line produces surges of greater magnitude than from an energizing operation.

The characteristic of switching surges summarized in Figures 1 to 6 were obtained on systems of various characteristics from the standpoint of grounding.

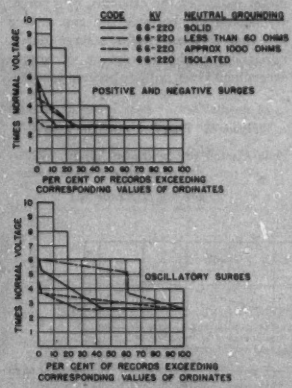


Figure 2. Cumulative percentage curves showing magnitude of recorded overvoltages due to switching

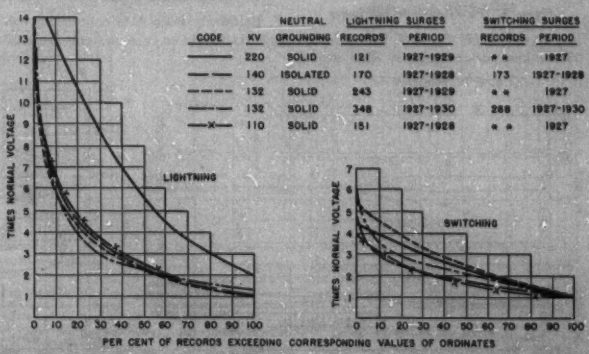
Average for 10-year operation period. Base voltage = $2^1/_2$ × normal at 100 per cent on abscissa

(*Figure* 32, *reference* 18)

Figure 3. Cumulative percentage curves showing magnitude of recorded overvoltages due to lightning and switching

Base voltage = 1 × normal at 100 per cent

(*Figure* 33, *reference* 18)



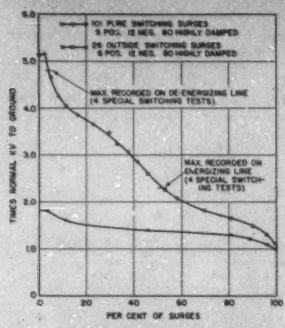


Figure 4. Cumulative percentage curves showing magnitude of recorded overvoltages from switching on the American Gas and Electric Company's Philo–Canton lines

Base voltage = 1 × normal at 100 per cent. Outside switching surges originate from switching on parts of the system other than where instruments are located

(*Figure* 10, *reference* 16)

Table II.* Showing the Variation in Surge Conditions on Individual Cable Systems

Figures are for One Year's Operation of a Three Terminal Klydonograph on Each System

System Cable and Open Wire, Kilovolts	Unidirectional Number of Surges: 1.1 to 2.5 Times Normal	2.6 and over	Maximum Times Normal	Per cent Total	Oscillatory Number of Surges: 1.1 to 2.5	2.6 and over	Maximum Times Normal	Per cent Total
24	13		2.4	68	4	2	3.0	83
26	30		1.7	100				0
26	250	30	4.3	91	27		2.4	9
26	140	7	3.3	73	27	26	4.6	27
33	12	4	3.1	80	2		1.8	11
66	215	13	4.7	96	10		2.4	4
Cable only								
11	140	2	2.6	91	15		1.8	9
13	320		1.8	98	5		1.3	2
13	320		1.5	97	11		1.4	3
13	600	9	2.6	100				0
13	105	2	4.3	69	48		2.3	31
26	38		2.6	44	46	2	2.6	56
33	87		1.4	100				0
45	125		1.6	97	2	2	2.6	3
	2,393	67		92	197	32		8

* From Table VI, reference 14.

The Joint Development and Research Subcommittee of the Edison Electric Institute and Bell Telephone System[18] made an extensive investigation for the correlation of system overvoltages and system neutral grounding impedances. As part of this study it examined the previously reported data from which Figures 1 to 6 were obtained. In addition, the results of observations made on several systems ranging from 26 to 140 kv were given. Figures 7 to 9 summarize the results on three of these systems in the form of scatter diagrams. The dotted straight lines in these three figures were statistically derived to determine the degree of correlation between the observed voltages and the distance between the point of observation and the fault. In the event of a ground fault the crest voltage on sound phases rises to a higher value for ungrounded systems than for systems grounded through a low impedance. The Joint Development and Research Subcommittee found that the sustained fundamental frequency overvoltages had an important bearing on the total crest voltages to ground that would be obtained. The maximum surge voltage of six times normal reported in Figures 1 to 3 may be limited by the wet flashover of the line insulators.[19] In lightning-arrester protected systems where lightning-arrester operation could have occurred, the maximum surge voltages would have undoubtedly been appreciably below six times normal.

Overvoltages Obtained During Field Tests on Cable Systems

Relatively little data concerning voltage transients on cable systems are available. Some results from studies on six combined cable and open-wire systems and eight "pure cable systems" are shown in Tables I and II.[14] In general it is indicated that switching operations on cable systems produce substantially lower surges than on open-wire systems.

The history of switching surges on the Chicago (Ill.) 12-kv system experienced during the period from 1919 to 1938 was reviewed and published,[20] and Figures 10, 11, and 12 are results from this study. Spark gaps and needle gaps were used in obtaining these data. The gaps were calibrated by breakdown and a recording meter indicated the existence of a surge. By correlation between the gap setting and time of occurrence of a surge, determination of the percentage of surges exceeding a given voltage became possible.

Figure 5. Pure line switching surges

May 10 to October 16, 1927; Philo–Canton 132-kv line; de-energized at Philo station

(*Figure* 12, *reference* 16)

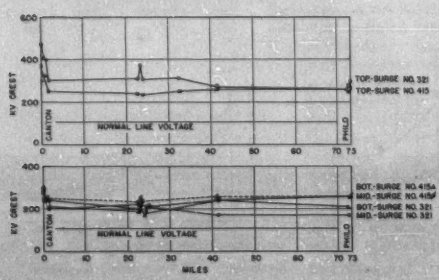
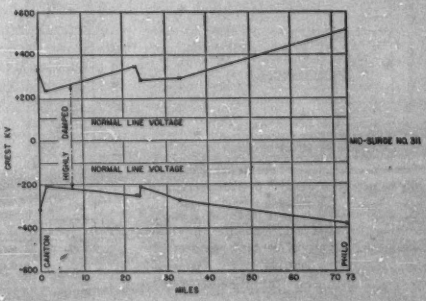


Figure 6. Pure line switching surges

Philo–Canton 132-kv line on July 30, 1927—11:00 p.m., de-energized at Canton station

(*Figure* 13, *reference* 16)



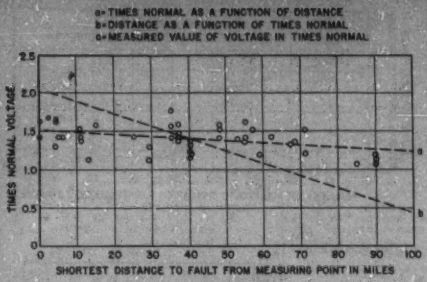


Figure 7. Public Service Company of Northern Illinois south zone system, 33-kv resistance grounded*

Probable curves of times normal voltage as a function of distance of faults from measuring point (46 cases)

(*Figure 28, reference 18*)

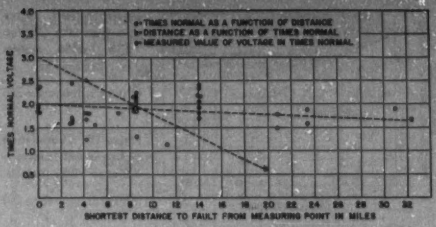


Figure 9. Public Service Electric and Gas Company 26-kv resistance grounded system*

Probable curves of times normal voltages as a function of distance from measuring point

(*Figure 27, reference 18*)

Table I and Figures 10 and 11 demonstrate that high surges accompany cable failure. In some cases coincident with a fault in the system a fault at another location occurred. This presumably was caused by the effects of voltage surges initiated at the first fault and acting on a progressively weakened cable insulation, or from cumulative increases in voltage produced by a circuit breaker restriking upon interrupting charging current of the cable. On the Chicago system during the period of observation 4.85 per cent of 1,240 primary failures were accompanied by one or more simultaneous faults. In no case did the surge exceed 3.5 times normal voltage. On systems with a combination of open-wire and cable surges higher than three times normal occurred, as shown in Table II. In Table I it is shown that faults and interruptions have substantially higher ratios of high surges than do other causes. It is surmised that the voltage of such surges is increased because of the confined path for cable puncture. Weakened insulation of aging cables and restriking circuit breakers are assumed to be a contributing factor in the production of simultaneous faults reported in the operating experience of cable systems. Switching surges are of importance on cable systems because of the large investments involved.

Overvoltages Obtained from Special Investigations of Switching Surges

In recent years there have been relatively few field investigations of the general problem involving the production of overvoltages as a result of switching operations or faults on power systems. Such work has been directed principally to determining the magnitude of overvoltages encountered in particular systems or with particular types of switching disturbances. There have been, however, two general investigations; namely, interruption of charging current, and transients in electric furnaces.

High-voltage circuit breakers are required to interrupt fault current quickly in eight cycles or less. They are also called upon to interrupt charging current for long sections of high-voltage lines. Considerable attention has been devoted recently to the transients produced by such switching operations since the surges are of low frequency and contain a great deal of energy. Thus they can permeate the entire system. These transients can be minimized by the use of stored energy type circuit breakers,[21,22] or the use of resistance elements shunting a portion of the interrupter. This latter means limits the number of restrikes and reduces the overvoltages produced when restrikes occur. Some test results showed on the

Figure 8. Consumers Power Company 140-kv isolated system*

Probable curves of times normal voltages as a function of distance from measuring point

(*Figure 25, reference 18*)

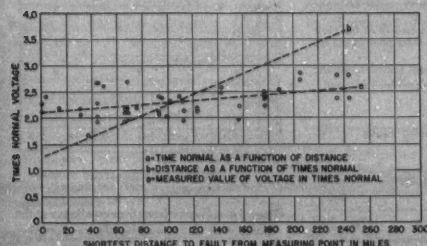
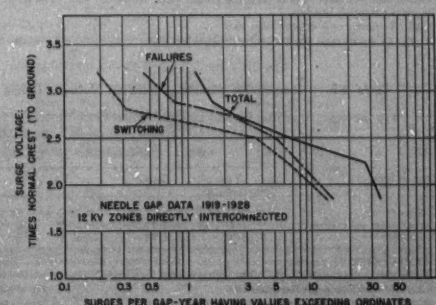


Figure 10. Surge data from the Chicago 12-kv cable system, 1919–1928

(*Figure 4, reference 20*)



* The dotted straight lines or regression lines were derived statistically to determine the degree of correlation between the observed voltages and the distance between the point of observation and the fault. Scattering of the points causes the lines to diverge and they would be 90 degrees to each other when there is no correlation among the points of the diagram. If all points fall on a straight line the regression lines will coincide and also pass through all the points.

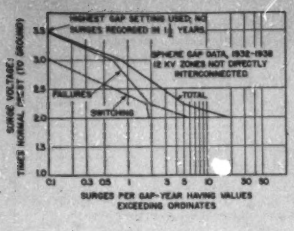


Figure 11 (left). Surge data from the Chicago 12-kv cable system 1932–1938

(*Figure 5, reference 20*)

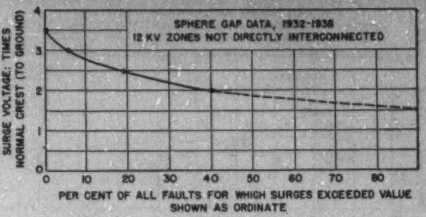


Figure 12. Correlation of surges and failures—Chicago 12-kv systems

(*Figure 6, reference 20*)

bus side a maximum voltage to ground at the switch of 1.9 times normal on de-energizing a 130-mile section of a 220-kv solidly grounded system.[23] Also it was reported that maximum voltages to ground for de-energizing a similar length of 220-kv line operated with solid grounding or with a ground fault neutralizer were as follows:

Maximum Voltage	Without Shunting Resistor	With Shunting Resistor
Ground fault neutralizer	4.4	2.0
Solidly grounded	1.8	1.3

In a recent paper, the results of tests made on a system of underground cable and overhead line to determine a design of the resistance shunting elements for the interrupters of the circuit breaker were described.[24] These studies were made to prevent possible failure of lightning arresters from voltage surges caused by restriking in the circuit breaker when switching, essentially, a capacitive circuit of cable and overhead line.

In another recent paper, results are given of many tests which showed that the maximum voltage at the bus on de-energizing long transmission lines did not exceed 2.5 times normal on a solidly grounded system and 4.4 times normal on a neutral reactor grounded system using circuit breaker designs without effective arc controlling means for the lower current values. With arc control means capable of limiting the number of restrikes to one or less during charging current interruption, the bus voltage on solidly-grounded systems did not reach two times normal.[25]

Mention should be made of investigations on the switching of large capacitor banks, which is closely related to the problem of interruption of charging currents, except for the differences that arise as a

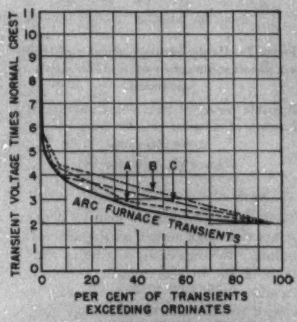


Figure 13. The distribution and magnitude of arc-furnace primary-voltage transients compared with switching and fault transient voltages

Base voltage = 2 × normal at 100 per cent

A and C—Switching transients
B—Transients from faults

(*Figure 7, reference 27*)

Table IIIA.† Comparative Fundamental Frequency Steady-State Voltages During Solid Faults

(Times Normal Line-to-Neutral Crest Voltages)

Type of fault	L-G V_N	L-G V_{LG}	L-L-G V_N	L-L-G V_{LG}	L-L V_N	L-L V_{LG}
Isolated**	1.0–1.3	1.73–2.1	0.5–0.6	1.5–1.6	0	1.0
Ground fault neutralizer	1.0*	1.73	0.5	1.5	0	1.0
Grounded***	0.6	1.4	0.5	1.3	0	1.0

† From reference 43.

* This voltage depends slightly on the fault location. The voltage at the neutral decreases as the fault location becomes more remote from the ground fault neutralizer coil. The effect is more pronounced for the more extensive systems.

** X_m/X_1 equals or is greater than 10.0

*** X_o/X_1 equals or is less than 3.0 at the fault

V_N = Voltage, neutral-to-ground
V_{LG} = Voltage, line-to-ground

Table IIIB.† Comparative Maximum Transient Voltages During Solid Faults

(Times Normal Line-to-Neutral Crest Voltage)

Type of fault	L-G V_N	L-G V_{LG}	L-L-G V_N	L-L-G V_{LG}	L-L V_N	L-L V_{LG}
Isolated**	1.7*–2.3*	2.7*–3.5*	0.7*–0.8*	2.0*–2.3*	0	1.0
Ground fault neutralizer	1.7*	2.7*	0.7*	2.0*	0	1.0
Grounded***	0.9*	1.7*	0.6*	1.7*	0	1.0

† From reference 43.

* These values depend on C_1/C_o. The values are for $C_1/C_o = 1.0$. Values are less for C_1/C_o greater than 1.

** X_m/X_1 equals or is greater than 10.0

*** X_o/X_1 equals or is less than 3.0 at the fault

V_N = Voltage, neutral-to-ground
V_{LG} = Voltage, line-to-ground

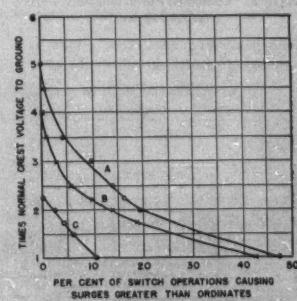


Figure 14. Comparison of overvoltages at the primary of arc-furnace supply transformer

A—Normal condition with system grounded through Petersen coil
B—System solidly grounded at three points
C—Underground cable on source side of circuit breaker replaced by overhead line

(*Figure 6, reference 28*)

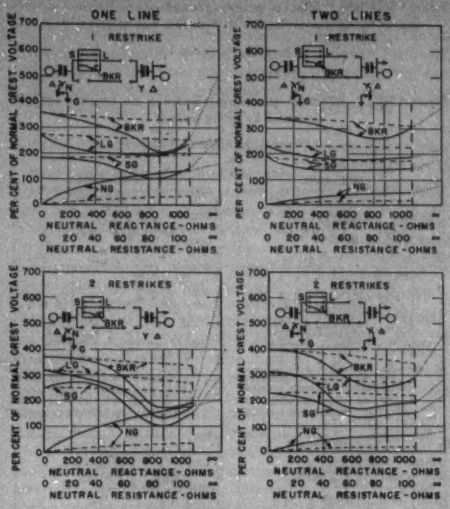


Figure 15(A).* Effect of grounding impedance on transient voltages caused by de-energizing an unfaulted line

(*Figure* 13, *reference* 19)

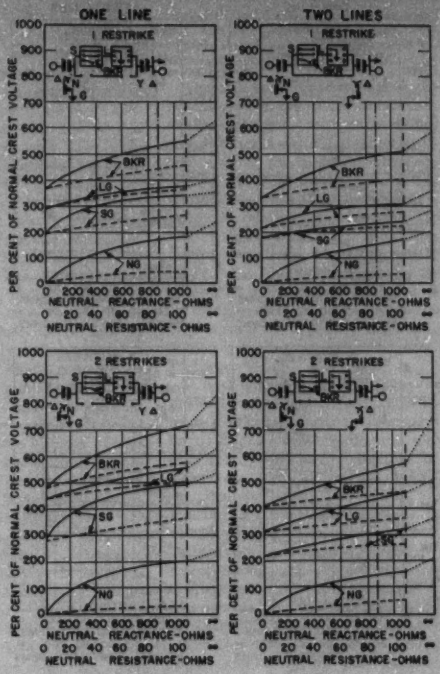


Figure 15(B) (right).* Effect of grounding impedance on transient voltages caused by de-energizing line with single line-to-ground fault

(*Figure* 14, *reference* 19)

Petersen-coil reactance: 875 ohms Solid curve: reactance grounding Dotted curve: resistance grounding

result of lumped capacitance in a capacitor bank in comparison with distributed capacitance on the transmission line and for the frequency of circuit breaker operations involved.[36]

The transient overvoltage incident to the operation of electric furnaces has been investigated by the AIEE subcommittee on transient voltages in arc-furnace circuits.[37] From Figure 13 and other data, the subcommittee concluded on the basis of voltages encountered in the supply system that the surges are similar to those produced by ordinary switching operations except for the increased frequency of occurrence. These transient overvoltages in arc-furnace installations occur during the melting-down operation and at the time of opening or closing the furnace circuit breaker. The highest magnitudes appear to be associated with circuit breaker opening operations. Mention should be made also of a special case reported when transients were minimized by elimination of a short section of cable in the primary supply as shown in Figure 14.[38]

There are in addition other special applications and unusual operating conditions which may give rise to overvoltages such as overspeeding of generators when disconnected from loads, or such as those resulting from the third and fifth harmonic components of voltage generated

Table IIIC.† Comparative Maximum Transient Voltages Caused by Switching*

(Times Normal Line-to-Neutral Crest Voltage)†

Circuit Breaker Restriking in Type of Fault	Faulted Phase		Unfaulted Phase					
	L-G		None		L-G		L-L-G	
	V_N	V_{LG}	V_N	V_{LG}	V_N	V_{LG}	V_N	V_{LG}
Isolated**	3.7	4.4	2.1	3.6	2.0	5.6	2.6	5.4
Ground fault neutraliser	1.1	1.8	1.5	3.1	1.8	5.2	2.3	5.3
Grounded***	0.7	1.0	0.4	3.9	0.6	4.6	1.3	4.7

† From reference 43.

* All of the results in this table were obtained in studying a particular system assuming two restrikes under the worst possible conditions. This makes the results directly comparable. Studies on other systems indicate that the relative values of voltages tabulated here are typical for most practical operating conditions.

** X_{ss}/X_1 equals or is greater than 10.0 V_N = Voltage, neutral-to-ground

*** X_s/X_1 equals or is less than 3.0 V_{LG} = Voltage, line-to-ground

* In the case of switching operations the circuit is initially opened at a fundamental current zero. The point of restriking is adjusted so as to give the maximum voltage for a given number of restrikes. The subsequent circuit openings are alway assumed to take place at the first current zero following the restrike. In the case of the arcing ground, the fault is applied at the crest of the normal line-to-ground voltage and is then removed at the first current zero. The point of restriking is adjusted so as to give the maximum voltage for the number of restrikes considered. The fault is always removed at the first current zero following each restrike.

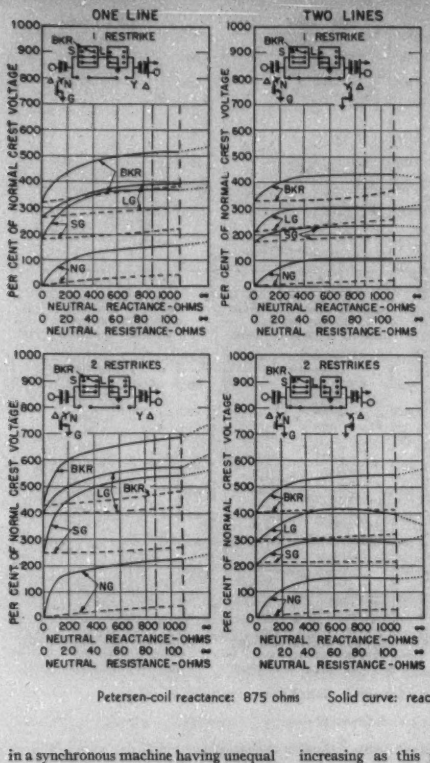
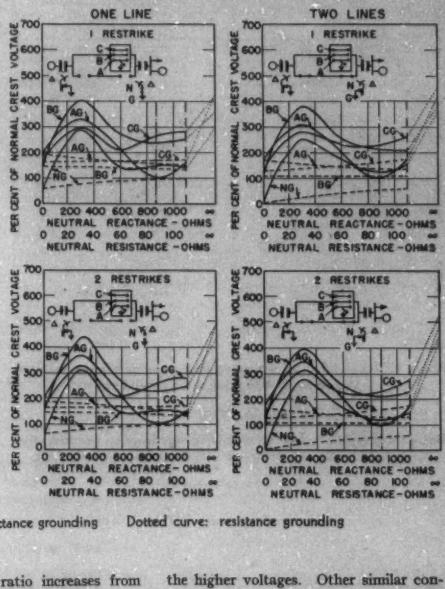


Figure 15(C) (left).* Effect of grounding impedance on transient voltages caused by de-energizing line with double line-to-ground fault

(*Figure* 15, *reference* 12)

Figure 15(D).* Effect of grounding impedance on transient voltages caused by arcing grounds

(*Figure* 12, *reference* 19)



Petersen-coil reactance: 875 ohms Solid curve: reactance grounding Dotted curve: resistance grounding

in a synchronous machine having unequal rotor impedances when an unbalanced fault occurs on overhead transmission lines with an appreciable amount of line charging capacity and when the lines were disconnected from the system or load.[29,30] In this latter case the harmonic overvoltages are known to be determined principally by the ratio X_q''/X_d'', the magnitude of the abnormal voltages increasing as this ratio increases from unity. Damper or amortisseur windings can be used effectively to reduce this ratio and prevent these abnormally high voltages. Rational design of apparatus and systems to prevent sustained overvoltages is essential to eliminate damage of lightning arresters and make possible the coordination of insulation levels and make possible a reduction in insulation levels at the higher voltages. Other similar conditions of overvoltage have been discussed by the AIEE committee on protective devices in its report "Correlation of System Overvoltages and System Grounding Impedances."[31]

Mention should be made of the AIEE report by the Subject Committee on Present-Day Grounding Practices, containing a review of operating experiences by numerous power systems with different grounding methods.[32] It is indicated that on a basis of the ratio of those systems reporting double-faults—simultaneous or consequential—to the total number of systems covered in the report,

Table IIID.† Comparative Maximum Transient Voltages During Arcing Faults

(Times Normal Line-to-Neutral Crest Voltage)

	Transient Arcing				Sustained Arcing			
	Clearing from Steady-State Fault Two Restrikes		Transient Arcing Ground Two Restrikes		Restriking at Definite Voltage Magnitude		Restriking Once Every Half Cycle	
	V_N	V_{LG}	V_N	V_{LG}	V_N	V_{LG}	V_N	V_{LG}
Isolated*	3.7	4.4	4.5	5.5	3.2	4.9	7.5	10.0
Ground fault neutralizer	1.1	1.8	1.2	2.2	1.3	2.4	1.1	1.8
Grounded**	0.7	1.9	0.8	2.0	0.8	2.1	0.7	2.0

† From reference 43.

* X_{c0}/X_1 equals or is greater than 10.0

** X_0/X_1 equals or is less than 3.0

V_N = Voltage, neutral-to-ground

V_{LG} = Voltage, line-to-ground

* In the case of switching operations the circuit is initially opened at a fundamental current zero. The point of restriking is adjusted so as to give the maximum voltage for a given number of restrikes. The subsequent circuit openings are always assumed to take place at the first current zero following the restrike. In the case of the arcing ground, the fault is applied at the crest of the normal line-to-ground voltage and is then removed at the first current zero. The point of restriking is adjusted so as to give the maximum voltage for the number of restrikes considered. The fault is always removed at the first current zero following each restrike.

about one-fourth of the ground-fault neutalizer systems, ungrounded systems, and resistance grounded systems reported double faults, whereas less than four per cent of effectively solidly grounded systems reported this type of trouble.

Results from Systems in Miniature

Prior to the accumulation of the principal data on switching surges, investigations had been started to determine the mechanism of cumulative oscillations and the phenomena involved in the interruption of an arc and its restriking.[33,34] These studies showed that high overvoltages could be produced by alternate establishment and interruption of an arcing path, if restriking takes place at successively higher values of voltage. The phenomena involved in restriking were shown to be dependent upon the intersection of two curves, one representing the magnitude-time characteristic of system recovery voltage across the former arc path and the other the magnitude-time characteristic of dielectric recovery strength in the arc path following a current zero.[35]

System-recovery voltages can be measured on a cathode-ray oscillograph on an actual power system in the field or on a miniature system in the laboratory.[36,37,38] The recovery voltage also can be calculated by theoretical methods, but the complication and tediousness have prevented analysis of sufficiently complex systems as to represent practical conditions.[38,39] The limitations of the foregoing methods were overcome by a method of analysis based on the use of the miniature system.[40,41] In this method the system under consideration is set up in miniature with sources of voltage and lumped constants of inductance, capacitance, and resistance to represent corresponding elements of the actual power system. Circuits with distributed constants are simulated by combinations of lumped inductance and capacitance elements. Switching operations are performed on the miniature system which correspond to the circuit change to take place on the actual system. Typical investigations in which the miniature system method has been used are the following.

1. Protector-tube recovery voltages.[42,43]
2. Transient overvoltages during faults.[19,44,45]
3. Arcing grounds.[19,46]
4. Circuit breaker restriking.[19,33,46,47]
5. Effect of lightning arresters in limiting overvoltages.[48]
6. Effect of transformer saturation in limiting overvoltages.[48,49]

In Figure 15 and Table III are summarized data from some of these investigations. Figure 15 shows the effect of grounding impedance on transient voltages caused by de-energizing (1) an unfaulted line, (2) a line with a single line-to-ground fault, and (3) a line with a double line-to-ground fault. The transient voltages are produced by one or by two cumulative restrikes in the interrupting circuit breaker. Table IIIC also shows the transient voltages caused by switching indicating the maximum values that may be experienced in the faulted or unfaulted phases. The values recorded are those produced by two restrikes in the circuit breaker under the conditions leading to the highest overvoltages on systems in which the system neutral is isolated, effectively solidly grounded, or grounded through a ground fault neutralizer. For the same conditions of grounding Table IIIA shows the fundamental frequency steady state voltages whereas Table IIIB shows the maximum transient voltages that may be experienced when a solid fault occurs on a system. Figure 15(*D*) and Table IIID show the overvoltages that could be produced by arcing grounds provided the postulated conditions of arcing and of recovery of the air dielectric were obtainable. The voltages shown in Figure 15 and Table III are those obtainable when no voltage limiting protective devices such as lightning arresters have been employed. These results demonstrate:

1. The magnitude of the steady-state fundamental-frequency overvoltage is directly associated with the arrangement of the system neutral and the type of fault. The lowest overvoltages are obtained on the effectively grounded system ($X_0/X_1 \gtrless 3$). The highest overvoltages are obtained on the isolated neutral system with single-line-to-ground faults and may be of the order of 1.73–2.1 times normal line-to-neutral crest voltage. The grounded system may approach 1.4 times normal.

2. The transient overvoltages during solid ground faults are always less than twice the steady-state overvoltages reached under the same fault condition. The maximum values are of the order 2.7 to 3.5 times normal on the isolated neutral and 1.7 times normal for the grounded neutral system.

3. Voltage surges of sufficient severity to damage equipment are obtainable for a large variety of system conditions during the interruption of line charging current when circuit breakers are used with no effective means to eliminate or minimize a possible process of cumulative restriking within the circuit breaker. The maximum voltages that may be experienced from two restrikes are of the order of 5.5 and 4.5 times normal for the isolated neutral and the grounded neutral systems respectively. It is to be emphasized that even under fault conditions one or two phases may interrupt only line charging current. These overvoltages are approximately the same as the maximum values recorded from field tests.

Where the lightning arresters limit the switching surge voltages, it is indicated that switching surge voltages should not exceed the sparkover voltage of the lightning arrester which, in an effectively solidly grounded system ($X_0/X_1 \gtrless 3$) should be the order of 2.5 times the line-to-neutral voltage or approximately the 60 cycle sparkover voltage. With lightning arresters being operated by switching surges it is shown that as a consequence the severity of the discharge on the lightning arrester in some systems can be of equal if not of primary importance as compared with expected lightning surge discharges.[48]

It is of interest to point out that the production of cumulative oscillations requires a dielectric recovery voltage curve for which the magnitude increases with time from the first current interruption. This characteristic is radically different from that obtainable on the ordinary line insulator of a transmission system. To date no oscillographic evidence of this characteristic has been observed[50,51,52] except during certain types of circuit interruption with circuit breakers.[33,47] The opinions that this characteristic can be obtained by a fault in solid insulation are based on insulation flashovers difficult to account for in any other manner, rather than on more direct evidence. There is also no evidence to date that an arc as a circuit element either in a circuit breaker or in a line-to-ground fault can produce overvoltages as the result of its nonlinear characteristics, although it has been postulated theoretically.[53,54,55]

On the basis of experimental evidence from the field and of miniature system studies the magnitude and severity of switching surges can be reduced as a factor in the insulation requirements of apparatus and lines by the following means:

1. Effectively grounding system neutrals ($X_0/X_1 \gtrless 3$).
2. Rational application of lightning arresters.
3. Switching short sections of line.
4. Switching lines on the low side of transformers.
5. Switching with stored energy type circuit breakers or with resistance shunting of the circuit breaker contacts.
6. Switching with circuit breakers designed to interrupt charging current with not more than one restrike.

Perhaps the principal advantage of the miniature system method is that it pro-

vides a rational approach to the problems of overvoltages to be expected for a wide range of system constants and grounding impedances. Results of this approach are more understandable and, no doubt, more useful than could be obtained from field data which are incomplete because such data cannot cover a great range of system constants or switching conditions.

Conclusions

1. Maximum switching surge voltages on overhead lines or cables are the consequence of the interruption of fault currents and/or the de-energization of an essentially capacitive circuit.

2. Maximum switching surge voltages of six times normal may be experienced on overhead lines. The maximum values experienced are perhaps limited by the flashover of insulators or bushings on systems where there is no lightning arrester operation from switching surges.

3. Maximum switching surge voltages of three and one half times normal may be experienced on cable systems in which no arrester operation is involved.

4. Items 2 and 3 of the foregoing are based largely on data accumulated since 1924 from measurements using gaps or the "Lichtenberg" figure principle of measurement and which therefore are of limited accuracy.

5. Transient overvoltages incident to the operation of electric furnaces are similar to the surges experienced on an overhead line except for the increased frequency of occurrence. The highest magnitudes appear to be associated with circuit breaker opening operations.

6. Direct evidence to date indicates that switching surges of the greatest severity on systems occur only from circuit interrupting devices wherein a process of cumulative restriking of an arc occurs at successively higher voltages as the result of an increasingly greater dielectric recovery strength. No direct evidence exists that this process occurs from arcs in open air or in solids.

7. There is no evidence to date to indicate that the nonlinear characteristics of the arc enters in as an important factor in system overvoltage.

8. The method of analysis using system representation in miniature, provides a rational approach to the problem of overvoltages and gives results that are more understandable than can be obtained from field data.

9. Results from the study of systems in miniature show that the magnitude and severity of the switching surge may be reduced by the following:

a. Effective grounding of system neutrals.

b. Rational application of lightning arresters.

c. Use of circuit breakers that have a minimum tendency to restrike.

d. Switching short sections of line.

e. Switching lines or cables on the low-voltage side of the transformers.

10. In lightning arrester protected systems, the switching surge voltage should not exceed approximately the 60 cycle sparkover voltage of the lightning arrester. For a solidly grounded system ($X_0/X_1 \cong 3$) this value is about 2.5 times normal line-to-neutral voltage.

11. In lightning arrester protected systems involving long lines, the severity of arrester discharge from switching surges can be of primary importance as compared with probable lightning surge discharges.

12. In lightning arrester protected systems the switching surge does not appear to be an important determining factor in the insulation requirements of apparatus or lines.

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76. Lightning Surges Transferred From One Circuit to Another Through Transformers, P. L. Bellaschi. AIEE Transactions, volume 62, 1943, December section, pages 731–38.

77. The Cathode-Ray Oscillograph Applied to Long-Time Switching Transients, G. W. Dunlap, H. Rohats. AIEE Transactions, volume 62, 1943, May section, pages 231–34.

78. Practical Calculation of Electrical Transients on Power Systems, R. D. Evans, R. L. Witzke. AIEE Transactions, volume 62, 1943, November section, pages 690–96.

79. Transmission-Line and System Problems in Supplying Large Electric Arc-Furnaces During War Time, B. M. Jones. AIEE Transactions, volume 62, 1943, April section, pages 197–202.

80. Abnormal Overvoltages Caused by Transformer Magnetizing Currents in Long Transmission Lines, H. A. Peterson, T. W. Schroeder. AIEE Transactions, volume 62, 1943, January section, pages 32–40.

81. Power System Voltages and Currents During Abnormal Conditions, R. L. Witzke. Transmission and Distribution Reference Book (Westinghouse Electric Corporation, East Pittsburgh, Pa.), third edition, 1944.

82. Transmission Line Voltage Surges, J. H. Cox. AIEE Transactions, volume 46, 1927, February section, pages 330–38.

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Discussion

Alexander Dovjikov (Bonneville Power Administration, Portland, Oreg.): The report is a very timely contribution to the art, and particularly so now when the tendency of large hydroelectric systems is to increase the reach of their high-voltage transmission lines. In order to do so economically, lines should be loaded more heavily than in the previous practice, and the lines should be designed with maximum use of materials commensurate with safe practice. An important item in decreasing the capital investment is the decrease of the insulation level of lines for voltages 230 kv and higher, and a review of causes of overvoltages on the system and of the measures to decrease them if not to eliminate them altogether is a fundamental approach to the problem. So far, the minimum amount of line insulation was based on extrapolation from low-voltage rules as given by the National Electrical Safety Code, approved by the American Standards Association, and it seems that this extrapolation is too far reaching and that the rules should be based on a more substantial engineering approach to the problem; a correct approach can be based naturally only on such work as presented by the subcommittee and on the experimental work on voltages above 230 kv being conducted in this country, in France, and in Sweden.

The system insulation must be co-ordinated, and such co-ordination begins usually with selection of the lightning arrester level. In so far as protection from lightning is concerned, we will give the benefit of doubt to the lightning arrester ability to protect the system from all but the most severe direct strokes accompanied by long waves. We are not certain, however, about the lightning arrester's ability to discharge overvoltages produced by certain faults and by switching operations, and the report recognizes that the surges produced by them "are of low frequency and contain a great deal of energy." Accordingly, conclusion 11 of the report states that "the severity of lightning arrester discharge from switching surge can be of primary importance as compared with probable lightning surge discharges." We agree that the correctly designed and correctly selected lightning arrester will protect the insulation on the first discharge impulse, but will the lightning arrester protect itself from destruction? Without a reasonable assurance (proved not only in the laboratory but also under actual operating conditions) that the lightning arresters will stand up in at least a large majority of transient over-

voltages that may be expected on the system, they cannot be taken as the basis for selection of lower insulation level on long, high-voltage lines with pronounced capacitance. We are not familiar with such investigations, and the extensive bibliography of the report hardly mentions any published work on the subject.

The line insulation must be above the sparkover voltage of the lightning arrester and above its voltage drop when discharging heavy currents; the seal-off voltage must be naturally above its 60-cycle voltage. The character of system grounding, or to be more exact the value of X_0/X_1, determines whether 80 or 100 per cent lightning arrester should be used. There is no magic, however, in the value of 80 per cent, and even a lower figure may be selected. The lightning arrester voltage rating, as determined by the ratio X/X_1, controls its protective level, and this value in turn governs the selection of station insulation level. The wet flashover of insulators, which should be assumed contaminated, must be above the lightning arrester protective level. How much higher? The insulator string should be of such dimension that no appreciable corona would appear on the insulator adjacent to the conductor, and the string should withstand the voltages appearing at the lightning arrester with a reasonable margin even when insulators are contaminated and one or two of them (depending on experience and adapted safety factor) has been damaged. If line insulation is reduced for a given degree of grounding and ground-wire protecton, a large number of outages due to insulation flashover and subsequent switching should be expected.

To what extent it is permissible is determined by the degree such switching affects the transmission of power, that is, speed of fault clearing, availability of reclosing, its type and speed, the number of parallel transmission circuits, and the relative change in transfer impedance produced by switching of faulted sections and other factors of maintaining transient stability of the system.

It is true that outside of some isolated cases the lightning arresters have stood up pretty well in the less extensive systems, and it may be reasoned that the switching surges were also taken care of. The increased extension of transmission systems, and the more frequent use of higher transmission voltages increase the possibilities of more frequent and more severe switching surges; these, particularly in connection with the proposed lowering of insulation levels, and the lower protective level of lightning arresters, require careful checking to insure against failure of existing lightning arresters.

The situation with lightning arresters resembles that with high-voltage transformers. Notwithstanding the fact that the transformers even of older construction had a very good operational record, new designs and new methods of insulation testing (impulse voltage) were introduced, and this line of approach is highly recommended for studies of operations and new developments of lightning arresters, which apparatus becomes more basic and therefore more important than ever.

W. M. Leeds (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The committee's report contains very valuable information and is a needed addition to the data on this important subject. With the increased use of cable, as well as longer overhead lines of extra high voltage, further studies will undoubtedly be warranted.

In the second paragraph under Section I (*D*), the statement is made that switching transients may be minimized by the use of stored-energy type circuit breakers or the use of resistance elements shunting a portion of the interrupter. A similar effect would be obtained from oil circuit breaker interrupters of the self-generated pressure type when provided with supplementary stored energy pistons to assist in the interruption of low currents.

Although it is generally true, as the report states, that de-energizing operations on a line or cable produce surges of greater magnitude than from energizing operations, it should be noted that, as shown by field tests, energizing transients superimposed on an already charged line or cable recently disconnected and left isolated may result in surge crests greater than obtained when de-energizing, even with a highly effective interrupter.

It appears that consideration, therefore, should be given to draining off such charges, particularly from an isolated cable of some length, before re-energizing.

S. B. Crary (chairman, AIEE general systems subcommittee): The discussions by Leeds and Dovjikov pointed out considerations which either have not been completely considered or which are outside the scope of the subcommittee report.

Leeds suggests that consideration should be given to the energization of isolated cables, and in particular to the draining off of the electrostatic charge. The residual electrostatic charge will be drained off more rapidly if switching is done on the low side of the transformers. In any case, it is necessary as Leeds has pointed out to consider the residual electrostatic charge and the rate at which it is drained off prior to re-energization.

The discussion by Dovjikov points out the importance of many other factors which it was not possible to include in the subcommittee's report. The problems which he mentions are important ones and some of them are being given careful consideration by other groups. It was the intent of the subcommittee report to confine itself to the magnitude of power system overvoltages and not to attempt to determine, for example, the amount of insulation required on the line or the specific conditions under which the lightning arresters may be damaged. It was pointed out, however, that the possibility for damage of the lightning arrester is reduced when the circuit breaker involved has a minimum tendency to restrike.

Mobile Radio

AUSTIN BAILEY
MEMBER AIEE

MOBILE radio as discussed in this paper will consider systems for extending voice communication to automobiles, trucks, and other vehicles. Radio-telephone communication with automobiles appears to have had its beginnings in the period immediately following World War I, at which time the motor car was assuming greater importance. Although law enforcement agencies were quick to adopt motor vehicles to increase their own mobility and effectiveness, it was apparent almost from the beginning that the police call-box wire telephone system which had worked well for the foot patrolman could be supplemented to advantage to meet the needs of motor patrols. One of the first applications of radio-telephony to automobiles, therefore, was in the field of police communication. Thereafter the success of these systems led to use by others having need for communication with automobiles, such as construction and repair trucks of power companies, fire and ambulance vehicles, busses, delivery trucks and taxicabs. Subsequently, mobile service was provided for the general user having need for telephone communication from motor cars by means of a common-carrier type of mobile service.

Early Police Radiotelephony

The first experiments in radio communication with automobiles appear to have been made in Detroit by the Detroit police department, during 1921. These experiments involved one stationary installation, called a "land station" in present-day terminology, and one radio-equipped car. The early experiments were not too successful, owing to the limitations of the radio apparatus then available for mobile use, and little further was done in the field of motor car communication at that time. In 1927, the Detroit police department renewed its experiments and by April of 1928 had a mobile radio communication system in successful operation.

The police department of Berkeley, Cal., independently conducted experiments with motor car radio communication in 1927, and in the spring of 1928 also had a radio system in operation in the neighborhood of 2,000 kc. It is reported[1] that the police cars in Berkeley were signaled by flashing neon lights and the officers were instructed by code to don earphones for secret messages or turn on loudspeakers for general broadcasts.

About this time a novel police radio system was improvised in Chicago.[2] Broadcast receivers were installed in police cars and radio broadcasting station *WGN* interrupted its programs to give messages to the police. This system soon turned out to be of as much value to criminals as to their pursuers, and gave way before long to an extensive police communication system patterned after the Detroit system.

Mobile radio for police was coming into general use in other parts of the western hemisphere as well. In the summer of 1933, the federal authorities in Brazil inaugurated a radio system at Rio de Janeiro for communication with motorcycle patrols and harbor craft of the marine police.[3]

The growth of municipal police radio was rapid indeed. By the end of 1929, 15 cities in the United States had one-way radio communication with police patrol cars, and two years later the total had grown to 70. In September, 1934, it was reported that 130 municipalities in eight states had police transmitters and about 4,000 police cars carried receivers.

The typical police radio system in operation about 1930 consisted of a small land transmitter, 500 watts or less, and a number of mobile receivers in automobiles. Operation was on a frequency in the 1,500–2,500 kc band. Usually the transmitter was installed in police headquarters, where the functions of the radio operator and police dispatcher could be performed by a desk sergeant or by the police telephone operator. The equipment was generally put on the air only for announcements, and not left on continuously as in broadcasting practice. Car receivers obtained filament voltages from the car battery and plate voltage from dry "B" batteries.

In the early 1930's it became increasingly apparent that the one-way nature of the communications from the land station to the mobile units was a serious limitation to the effectiveness of police radio. The opening of experimental frequencies in the 30–40 megacycle band at this time, although done primarily to make more channels available and relieve overcrowding on the medium frequencies, stimulated experimentation with small transmitters on cars, since at the higher frequencies the dimensions of a suitable radiating system became small enough to be applied to a motor vehicle.

Experiments with these mobile transmitters gave promising results, although with techniques available at the time the ranges obtained were seldom greater than three or four miles. These short ranges made it necessary to employ a number of land receivers, connected to the central operating point by wire lines. The quasi-optical nature of transmission at these frequencies placed a premium upon antenna height at land receiver sites and substantially line-of-sight transmission paths between cars and land stations.

Experiments having proved the utility

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AUSTIN BAILEY is with the American Telephone and Telegraph Company, New York, N. Y.

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of the 30–40 megacycle band for car-to-land transmission, manufacturers were not long in building and installing the new equipment. Some police departments supplemented their existing medium-frequency land-to-car systems with the higher frequency car-to-land installations, while others elected to employ the higher frequency equipment for both directions of transmission. According to one periodical,[4] the first commercial two-way installation for police use on these higher frequencies was made in the early summer of 1933 in Eastchester, N. Y. Another early two-way installation was made for the Bayonne, N. J., police department using a frequency of 34.6 megacycles, with 25 watts power at the land station and 4.5 watts in the police cars.[4] Power utilities, traction companies and others having use for emergency communication with their repair trucks also were providing radio-telephone installations. An installation for emergency radio-telephone service which for the first time provided connection from moving vehicles to any telephone was established in New York City and began operation on December 5, 1940.

The Beginnings of Modern Mobile Systems

In 1939, experiments indicated that frequency modulation (f-m) held much promise and appeared to be superior to the conventional amplitude modulation (a-m) universally employed up to that time for radio-telephone service from vehicles. In addition to giving some reduction in received noise, f-m was demonstrated to be capable of reducing the "flutter" noticed when receiving a-m signals in a vehicle moving at high speed. Thus f-m appeared to be particularly applicable to mobile radio.

Much of the pioneering work in f-m mobile radio was done in developing a state-wide radio system for the Connecticut state police.[6,7] The development work was begun in July, 1939, and the following year the system was in operation, using a frequency of 39.18 megacycles for car-to-land transmission and 39.50 megacycles for land-to-car and car-to-car. The use of two-frequency mobile units in this frequency range was in itself an innovation, as police systems in general had been operated previously on a single-frequency basis with car-to-car communication on the same channel as land-to-car transmission. The 2-frequency system provided greater flexibility of operation in the territory covered by a single land station, but its principal merit was that it permitted the various land stations to carry on their separate activities simultaneously without serious interference. Being on a separate frequency, the low-powered mobile transmitters in poor locations did not have to compete with land transmitters in nearby areas advantageously located on mountain tops.

While Connecticut was pioneering in f-m for state police use, the city of Chicago in March of 1940 was deciding on f-m mobile transmitters after a comparison trial of both f-m and a-m equipment.[8] Following the lead of Chicago and Connecticut, others turned to f-m, and most manufacturers of mobile radio equipment converted their production to f-m. The manufacture of a-m mobile systems was all but discontinued, a very small quantity of a-m equipment being produced chiefly for replacement purposes.

At this point World War II intervened and the full output of all radio manufacturers was diverted to military purposes. Although the war appeared to be a period of enforced inactivity, in so far as progress in civilian mobile radio was concerned, the rapid advances in electronics under the impetus of the war held a promise for future improvements of great significance.

Modern Mobile Radiotelephony

Advanced techniques, overcrowding in the 30–40 megacycle band, and opening of the 152–162 megacycle band all contributed to a move toward still higher frequencies by manufacturers and users alike. The 152–162 megacycle band, with its relative freedom from noise and long-range interference, and with other desirable advantages such as a car antenna less than 18 inches long, was extremely attractive. The facilities of manufacturers were taxed by the demands of police, and by power companies, taxi operators, and others.

Typical Present-Day Mobile System for Small Areas in the United States

The antenna most commonly used for small systems is a single coaxial dipole radiator, fed through an air dielectric or solid dielectric coaxial line. Formerly air dielectric was more popular, but with the development of low-loss solid dielectric line which will not increase its attenuation with age, the solid dielectric line is finding increasing favor. Where coverage requirements are unusually stringent, vertical directivity, obtained with a 2-bay or 3-bay stacked antenna, may be employed in place of the simple dipole to increase the effective radiated signal in the horizontal plane. For special coverage configurations, reflectors and directors are sometimes used to obtain horizontal directivity.

Operation of a small radio system is usually on a single-frequency basis as indicated by Figure 1; that is, all land and mobile transmitters and receivers are tuned to a single frequency. This permits communication car-to-car as well as land-to-car and car-to-land. If the community is one of a cluster of small municipalities, and if the given channel assignment is repeated in a nearby town, then it is customary to resort to 2-frequency operation to reduce interference. The land transmitter and mobile receivers will be on one frequency and the mobile transmitters and land receiver on another.

The usual small station employs a combined transmitter-receiver unit mounted in a console and installed on the back of a desk. The console cabinet contains, in addition to the transmitter chassis and receiver chassis, a loud-speaker, antenna transfer equipment, operating keys, pilot lights, and possibly meters. The radio transmitter-receiver-console for a small radio system is frequently installed adjacent to the telephone switchboard. By this arrangement the switchboard operator may also function as dispatcher-radio operator.

The operator may employ a telephone handset with push-to-talk button, or a desk stand microphone. The microphone may be used in conjunction with a transmit-receive key, a push-to-talk button, a foot switch or a knee-actuated talk switch. The loudspeaker is generally arranged to be in operation continuously; however, if a telephone handset is used, there is an option of muting the speaker when the handset is removed from its cradle.

The land transmitter most generally used for a small system is rated at 50 watts output. In rare instances a 250-watt transmitter may be required. The operating frequency usually is in the 152–162 megacycle band, the center frequency being maintained within 0.005 per cent of the assigned frequency by crystal control. The land receiver is also crystal-controlled, and for satisfactory operation on an alternate channel basis (stations two 60-kc channels, or 120 kc apart) the receiver selectivity should be about 50 or 60 decibels down at 60 kc off frequency and 85 to 100 decibels down at 120 kc off frequency. Audio output is usually a watt or two. Receivers are now universally equipped with some form of "squelch" to disable the radio receiver in the absence of a received signal and thus eliminate noise

reception during the "no signal" intervals. Both transmitter and receiver operate from 115-volt 60-cycle commercial a-c power.

The typical mobile installation is shown in Figure 2. The mobile transmitter and receiver units are generally almost identical with the land station equipment, except that the power output may be less and the primary source of power is obtained from the 6 volt car battery. Plate voltage for the receiver is obtained by a vibrator power supply, and a dynamotor is generally used for transmitter plate power.

The typical system just described has many applications. It is used by small police departments, by taxicabs, power companies, and other enterprises employing a number of mobile units. For some applications, talk back is not sufficiently important to warrant the additional expense. In such a case the mobile units may be operated on a receiving only basis, that is, vehicles would be equipped only with receivers, and only a transmitter would be required at the land station.

System for Larger Service Area

In a larger city it is found quite often that the operating point is not located in the most prominent structure in the downtown area, but is overshadowed by much taller office buildings. This, plus the much larger service area to be covered almost always makes it necessary to choose some higher location for the land station antenna. In many instances space for the radio installation is leased in the top of the tallest available office building. If there are high hills or mountains immediately adjacent to the city, the top of one of these may be selected as the radio site.

Since the transmitting and receiving equipment must be relatively close to the antenna to minimize the length of transmission line and its attendant losses, and the operating point will be at another location, some form of remote control is required. The usual arrangement makes use of a wire telephone line between the operating point and the radio transmitter-receiver site. The direction of operation of the line is reversed by the operator from the control point, through the use of a suitable control circuit.

When the land station is in the normal, or receiving condition, the audio output of the remote land receiver is fed over the line to the operating point where the level is raised by a line amplifier and fed to a loud speaker. For transmitting, the output of the microphone is amplified by a line amplifier, sent over the telephone line to the radio site and applied to the input of the land transmitter. The transfer of the wire line from receiver output to transmitter input, as well as the antenna transfer function, are under control of the aforementioned control signals actuated by the push-to-talk switch. The equipment units required at the operating point to provide remote control are customarily assembled in a console-type apparatus cabinet, called a "remote control console," which is installed on the operator's or dispatcher's desk. In addition to the features needed for local control which were previously outlined, the console probably will contain two line amplifiers, one for transmitting and one for receiving, or a single reversible amplifier to perform both functions. Provisions are made in remote control consoles, also, for monitoring the transmitter output. Usually, a small receiver tuned to the transmitter frequency is used, with some type of visual indication, often in the form of a "carrier-meter." Visual monitoring is most convenient, as it obviates the possibility of acoustical feedback from the monitoring loudspeaker into the microphone.

Another feature often found in remote control consoles is the "attention tone" signal. This is merely an audio oscillator whose output can be used to modulate the transmitter. The tone is generally sent out for a few seconds immediately preceding announcements as a means of attracting attention. This audio oscillator may also be keyed by a telegraph key if telegraphic communication is desired.

If the area to be served by the radio system is very large, and it is not possible to achieve full coverage satisfactorily with one land receiver, it may be necessary to install additional remote receivers. The outputs of these receivers can be multiplied at the operating point, or means may be provided for manual or automatic selection of the best receiver.

Systems for large cities are generally operated on a 2-frequency basis as indicated in Figure 1 with some, but not necessarily all, of the mobile units equipped for car-to-car communication. There may also be some vehicles equipped for receiving only. The mobile equipment

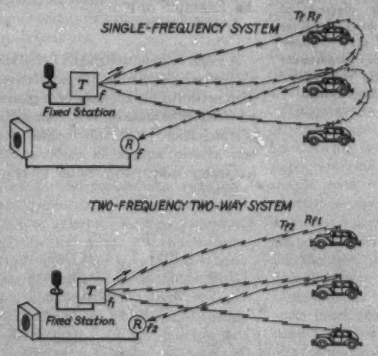
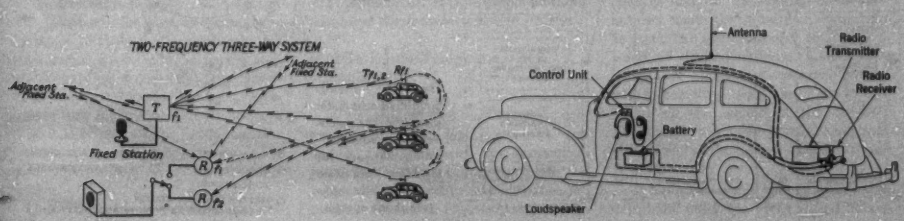


Figure 1. Methods of operating mobile radio telephone systems

Figure 2 (below). A typical mobile radio telephone installation



will be the same as described previously for smaller communities.

More Comprehensive Systems for Large Cities

An example of a comprehensive layout, recently engineered for the Albany, N. Y., police department is illustrated by Figure 3. This radio system illustrates the way telephone techniques may be employed to increase the flexibility and utility of conventional radio facilities.

In the Albany system, the radio land station is under control of a dispatcher-operator, who normally directs the operations of the motor car patrols. In addition, the dispatcher can give other officials at remote points access to the radio channel. Those having access are the police chief, police commissioner, chief of detectives, and the officers in charge of the six outlying precinct stations. The community of Rensselaer, across the river from Albany, is served also by the Albany police radio and the Rensselaer police headquarters has access to the radio channel. In addition, the mayor of Albany and the traffic department are provided with monitoring facilities so that they may hear all traffic carried on over the system.

In the operation of the system, picking up a handset at one of the remote locations and pressing the push-to-talk button lights a lamp on the dispatcher's key box. By depressing the associated key, the dispatcher connects the remote station to the radio facilities. Each remote station is equipped with loudspeaker monitoring equipment which is normally in operation but is automatically muted by picking up the handset. A novel feature is the "executive override" which permits the police chief, police commissioner and chief of detectives to break in on the radio channel when it has been switched by the dispatcher to one of the precinct stations.

State-Wide System

State police, large pipe-line and electric power companies and others operating fleets of vehicles on a state-wide or regional basis need mobile communication systems consisting of a number of land stations located to provide state-wide coverage and associated with a larger number of mobile units. The land stations may be connected together by a teletype or a voice-frequency wire network or both. The number of land stations required may be small if transmitting sites on high mountains are available and if maximum coverage with a minimum outlay is the only consideration. Often, however, it is desirable to have the coverage from each land station conform approximately to the boundaries of an existing administrative area. In such instances the number of land stations may be considerably increased, although the coverage requirements for each station may be correspondingly diminished. Still a third possible scheme consists in providing state-wide coverage with a few high-power stations strategically located on mountain tops, and supplementing these with low-powered installations. Traffic of a more general nature is handled by the high-power stations, and local matters are handled via the low-power stations.

Such systems are usually operated on a 2-frequency basis to permit simultaneous usage of several land stations with minimum interference. Some systems are two-way, but the so called 3-way systems illustrated in Figure 1 and similar to that employed by the Connecticut state police involving two-frequency mobile transmitters for car-to-car communication, are becoming increasingly popular. In conjunction with 3-way operation, it is often the practice to provide a second receiver at each land station location, tuned to the land station frequency. This permits monitoring of car-to-car communications by the land station and also makes possible emergency point-to-point communication between land stations.

An Example of a Statewide System

A radio system designed to give state-wide coverage with use of voice frequency wire networks has recently been placed in service for the state of New York division of state police. The radio facilities of this new system replace an earlier medium frequency a-m 1-way radio system in

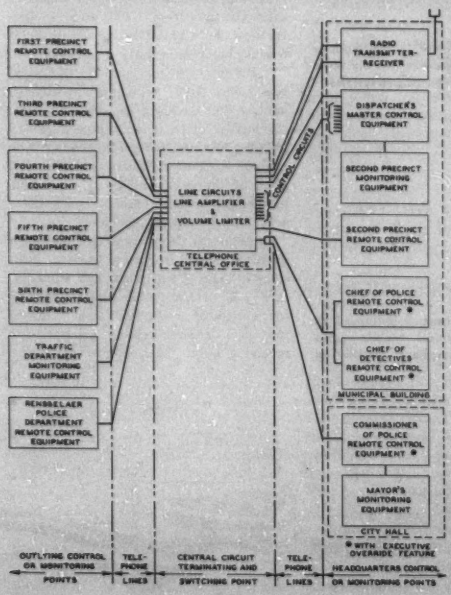


Figure 3. Simplified block diagram of telephone facilities for the Albany police department mobile radio telephone system

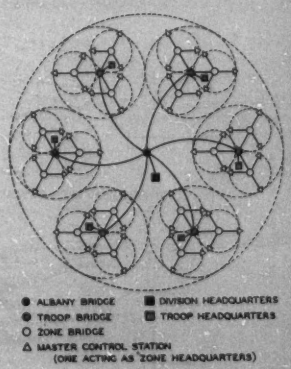


Figure 4 (right). Simplified schematic diagram of the voice-frequency communication network for the New York state police mobile radio telephone system

Figure 5. The components of a mobile radio telephone installation

northern New York state and a 2-way f-m radio system used on Long Island.

The radio stations of the new system are grouped so as to give coverage for police cars in each of the 18 zone areas into which the state is divided for direct administrative purposes. Each radio station is directly connected for radio control purposes to one of the police locations in the zone it serves. At that point the radio circuit facilities may be connected to a wire network extending to all other police locations in the zone. The zones in the state are grouped to form six troop areas and each troop headquarters may be connected with the zone networks of its troop area. The division headquarters at Albany may also be connected to the wire network of each troop and thence to the zones throughout the state. Thus, the operation of the facilities is consistent with the organization as shown on Figure 4. At the several headquarters, zone, troop or division, a person may talk over all the facilities under his organization and control or monitor all transmissions made over such facilities.

The radio stations within each zone are normally in the receiving condition ready to pick up calls from patrol cars. By operation of a key the incoming signals may be heard over the connecting wire network at other police locations in the zone or at troop headquarters where monitoring loudspeakers are available. On instructions from Albany, any or all of the zone networks may be connected through to that point. Since the entire system is operated on a push-to-talk basis and the direction of transmission on all circuits is automatically reversed by the push-to-talk button at a control point, messages from division headquarters may be heard by all patrol cars in the state or in any portion of the state, as desired.

At each of the police locations where a radio station is controlled, a person may talk directly over the radio circuit, may let the network monitor these transmissions or may himself monitor the network while it is being used for communication between the several police locations in the zone entirely separate from any radio connection.

This radio and voice frequency wire network supplements an extensive teletype network connecting all state, and many municipal, police locations in the state.

Common Carrier Mobile Services

While many automobile operators may have a vital need for communication, the scarcity of assignable frequencies and the costs of a private system are such as to make private systems often impracticable. By obtaining service from a common carrier operating a central radio station, it becomes possible for a number of such operators to obtain the needed communication. The combination of wire and radio telephone communication circuits, using wire circuits where possible and radio circuits for connection to the mobile units, offers an opportunity to provide a logical extension of telephone service to large groups of mobile units. In addition, an arrangement of this kind at once makes it possible for the automobile operator to talk with any telephone subscriber connected to the land wire network rather than only to specific individuals at telephones which are part of the equipment in a private system.

The extensive use by the army and navy of communication with mobile units on land, sea and in the air greatly stimulated public interest in communication with mobile units. This created an unprecedented demand for mobile telephone service as a part of the facilities available for normal business operations. Following the war, a limited number of frequencies were made available in the United States in both the 30–44 megacycle band and the 152–162 megacycle band for the experimental development of a common-carrier type of mobile radio telephone communication.

Mobile Radio-Telephone Equipment

It was evident that any common carrier system would need to provide the mobile subscriber with an instrument which in use would approach closely the telephones commonly employed by the public. One of the most important additions required was a calling arrangement which when actuated by signals sent out from the central land station would ring a bell in the automobile of only the one subscriber with whom communication was desired. Since various customers were to be served on the same channel, it was out of the question to employ the "voice-loudspeaker calling" generally in use on private mobile systems. A selective signaling system had been developed for use on small coastal ships and had proved to be extremely reliable. This was adapted for mobile service as the most readily available at that time.

Selective signaling of mobile subscribers is accomplished by means of a selective-signaling oscillator at the control point and a selector in the mobile unit. The selective-signaling oscillator provides a code consisting of a series of alternate pulses of 600 and 1,500 cycles which are transmitted at a uniform rate under control of a standard telephone dial or sender at the toll switchboard. Selective circuits in the selector of the mobile unit alternately respond to the 600 and 1,500-cycle tones and operate a stepping relay which actuates a Gill selector to rotate its contact wheel. Five code pins are placed in the contact wheel in positions corresponding to the five digits which comprise the telephone number of the particular selector. When any digit is dialed the contact wheels of all mobile stations receiving the transmission will advance a number of steps corresponding to the digit dialed. Only those wheels, however, that have a pin at the point of advancement corresponding to the digit dialed will hold, and the remainder will return to normal. Successive dialing of digits causes all selectors to again advance. However, only the selector which progressively advances on all five digits causes a bell and lamp to operate in the automobile.

Other modifications made in the commercially available radio equipment were to improve reliability, increase safety and provide for ease in maintenance. Although the equipment in the automobile was otherwise much the same as described previously for private systems, these changes and the use of a hang-up type handset made the subscriber equipment as much like one commonly employed as could be devised without extensive development. The one unusual feature which remained was the push-to-talk switch in the handle of the handset. Developments aimed toward the ultimate elimination of the push-to-talk switch are now under way.

The adaptation and use of mobile radio-telephone units has given added knowl-

Table I. Technical Characteristics of Mobile Equipment Recommended for Bell System General Mobile Telephone Service

Number	Item	Highway Service	Urban Service	Notes
1.	Frequency range			
	transmitting, megacycles	42–44	152–162	
	receiving, megacycles	30–40	152–162	
2.	Maximum frequency tolerance (−20° to +55° C), per cent	0.01	0.005	
3.	Type of modulation	phase	phase	
4.	Predistortion (referred to f-m characteristic) decibels per octave	6	6	
5.	100 per cent modulation swing, kilocycles	±11 (±7.3 radians)	±15 (±10 radians)	
6.	Audio band transmitted, cycles per second (see Item 25)	300 to 3,000	300 to 3,000	
7.	Maximum radio band width, kilocycles (tolerance plus modulation)	40	60	
8.	Output power, watts	20–40	15–35	
9.	Maximum transmitter spurious radiation	Ultimate—0.1 microwatt (equals 85 decibels below present carrier power) for frequencies within 20 per cent each side of the carrier. One microwatt (equals about 75 decibels below present carrier power) at all other frequencies. Temporarily acceptable—3 microwatts (equals about 70 decibels below present carrier power)		
10.	Radio transmitter input impedance, ohms	40	40	
11.	Radio transmitter output impedance	Suitable to feed 70-ohm transmission line with standing wave ratio up to two		
12.	Maximum residual transmitter noise modulation	The measured residual transmitter noise should be 47 decibels below the level of a 1,000-cycle sine wave tone which modulates the transmitter the full radian swing.		See Note Following Item 21
13.	Receiver sensitivity			See Items 17 and 18
14.	Receiver acceptance band, kilocycles (6 decibels down limits)	Ultimate—±10 to ±15. Temporarily acceptable: ±10 to ±25	Ultimate—±20 to ±25. Temporarily acceptable: ±20 to ±50	
15.	Receiver selectivity	Ultimate—85 decibels, adjacent mid-channel. Temporarily acceptable: 60 decibels, alternate mid-channel	Ultimate—85 decibels, adjacent mid-channel. Temporarily acceptable: 60 decibels, alternate mid-channel	
16.	Receiver spurious responses	Ultimate—85 decibels down. Temporarily acceptable: 70 decibels down	Ultimate—85 decibels down. Temporarily acceptable: 70 decibels down	
17.	Limiter effectiveness, decibels	30	30	
18.	Signal input for complete limiting, microvolts	5	5	
19.	Maximum "squelch" sensitivity, microvolt (adjustable inside equipment over 25 decibels range)	1	1	
20.	Radio receiver noise figure, decibels	12	8	
21.	Maximum residual receiver noise (car engine operating)	The measured residual receiver noise (car engine operating) should be at least 34 decibels (ultimately 50 decibels) below the level of a 1,000-cycle sine wave tone which modulates the test radio frequency signal, the full radian swing which is given in Item 5.		The noise should be measured in a manner which takes account of its interfering effect on a telephone circuit. This can be done by using a meter having a flat transmission characteristic and dynamic characteristics similar to the standard volume indicators* (a volume indicator or a noise meter may be used) in combination with a frequency weighting network. Figure 9 shows the characteristic of the weighting network commonly used in telephone work. Successive measurements are made first with the test radio frequency signal modulated with 1,000-cycle tone then with no modulation. In measuring residual noise in radio transmitters the same general method may be applied by using a radio receiver with very low residual noise to demodulate the transmitter signal.
22.	Radio receiver de-emphasis (referred to f-m characteristic) decibels per octave	6	6	
23.	Audio output impedance, ohms	600	600	
24.	Radio receiver output power, milliwatts	250	250	This assumes loudspeaker operation is not required. 250 m.w. will operate a selector set (see Item 26). The handset receiver requires 10 m.w.
25.	Audio characteristic (with 100 per cent modulation) of transmitters or receivers, cycles per second	1,000, reference 250, 0 to −4 decibels 500 to 2,000 +1 to −3 decibels 2,000 to 3,200 0 to −4 decibels	1,000, reference 250, 0 to −4 decibels 500 to 2,000 +1 to −3 decibels 2,000 to 3,200 0 to −4 decibels	The characteristic of a transmitter or a receiver is specified in terms of its departure from the ideal 6 decibels per octave pre-emphasis or de-emphasis, respectively.

26. Selective signaling. The selective signal receiving unit (selector set) must respond to the 2-tone audio-frequency selective signaling code transmitted by the land station.

The transmitted code consists of a series of pulses of two tones, 600 and 1,500 cycles per second, which are transmitted alternately at a uniform rate of 8 to 11 per second. The tones are transmitted successively without interruption and all pulses are of equal length (0.09 to 0.125 second.)

The current code plan for 2-way general mobile service** is based on a 23 pulse series, that is, a total of 23 pulses closes a local signal contact in the selector set. Coding is accomplished by breaking the sequence of 23 pulses into five groups, 2 to 10 pulses per group. Each of these groups corresponds to a digit (digit "0" corresponds to 10 pulses), in a 5 digit code number the sum of whose digits is 23. The digit "1" is used only as a "clearing" or preparatory signal to restore all selectors to their rest position and is therefore not available for use in any code number. One pulse or digit "1", is normally transmitted immediately preceding the transmission of a code number to make sure that all selectors start from the rest position. It is also transmitted again after the code number has been transmitted to release the called selector and silence the bell after a reasonable ringing interval.

Table I (continued). Technical Characteristics of Mobile Equipment Recommended for Bell System General Mobile Telephone Service

The local signal circuits associated with the selector set should provide a bell of suitable audibility and a lock-in lamp signal. The bell circuit should be arranged to remain operated (bell ringing) as long as the selector contact is closed. The lamp circuit should operate when the selector contact closes and remain operated (lamp lighted) until the local circuit is restored by the operation of a release button or contact.***

Proper utilization of the available code numbers without conflict necessitates centralized administration of code assignments. Code number assignments for general mobile telephone service must be obtained from the local telephone company.

27. Full operation of radio transmitter required within 300 milliseconds after "push-to-talk" button in hand set is depressed.
28. Nominal battery voltages either 6 or 12 volts. All characteristics should be met with battery terminal voltages between 5.7 and 7.5 volts for 6-volt system and between 11.4 and 15 volts for 12-volt system. Equipment should tolerate for ten minutes without permanent damage voltages of 5.0 or 8.5 for the 6-volt system and of 10 or 17 volts for the 12-volt system. Equipment should tolerate one second reductions of power supply to one-third of nominal voltages.
29. The equipment should operate satisfactorily with ambient temperatures ranging from −40 to +55 degrees centigrade (−40 to +149 degrees Fahrenheit.)
30. The voice frequency control unit used for Bell System installations will generally include, as mentioned in Item 27, a push-to-talk hand set, (Western Electric Company *F3WW*) equipped with a carbon button-type transmitter unit and a telephone-type receiver unit and having a 5-conductor cord. (In some few cases a radio-type microphone such as the condenser type and a loudspeaker may be used on a push-to-talk basis.) The frequency characteristics of the hand set (or equivalent talking and listening apparatus) should be such as to utilize efficiently the voice frequency band specified in Item 6. The control unit should also include, as indicated under Item 26, a lamp and signal bell to indicate incoming calls and a power control switch and associated indicating lamp.

* American Standards Association *Standard* C16.5, 1942.

** One-way signaling service will make use of codes totaling 23 pulses. The first digit of such code numbers will always be "9."

*** The Western Electric Company *106A* Selector Set (distributed through Graybar Electric Company) is designed to operate with the selective signaling system described before. It has a second local circuit contact closed by a total of 25 pulses. A minimum of 100 milliwatts at 600 cycles and 1,500 cycles is required to operate the number *106A* Selector Set. Its input circuit includes a transformer with 2-ohm and 500-ohm taps. A 100–250 volts direct current supply capable of delivering 2 milliamperes is required (this is usually obtained from the receiver power supply).

edge and experience in the desirable characteristics for these units. A table of the most recent views concerning the desirable electrical characteristics is given in Table I. The components of a mobile installation specifically designed for this service and having the desirable characteristics are shown in Figure 5.

Land Station Radio-Telephone Equipment

As in the case of mobile units, in providing land station installations the process also was one of adapting available equipment to new conditions. Special automatic monitoring means were added to the land station transmitters. A relay was added to the land station receivers which would operate when the "squelch" of the receiver indicated the reception of a signal. This relay in turn sends a control signal over a wire circuit which finally actuates a call-indicating lamp in the toll switchboard.

Control Terminal Equipment

The control terminal is in many respects the counterpart of the remote control console described previously. However, it is fully automatic and furthermore provides receiver selection, testing facilities, and alarm circuits for the radio units of the system. This control terminal is a simplification of the type which has been in use for a number of years on the radio-telephone circuits for coastal and harbor vessels. The conventional hybrid coil and the vogad (voice-operated gain-adjusting device) are the most essential features of the control terminal as it is possible by their use to combine effectively a transmitting circuit and a receiving circuit into a 2-way talking circuit for connection as another line into a toll switchboard of the telephone network. Figure 6 indicates in schematic form how the several parts of the common-carrier type of mobile radio-telephone system are integrated into a single operating whole.

Extent of Development

Systems such as have been described for common-carrier use have been put into operation at a number of places throughout the United States since the first one in St. Louis, Mo., was activated on June 17, 1946. The map of Figure 7

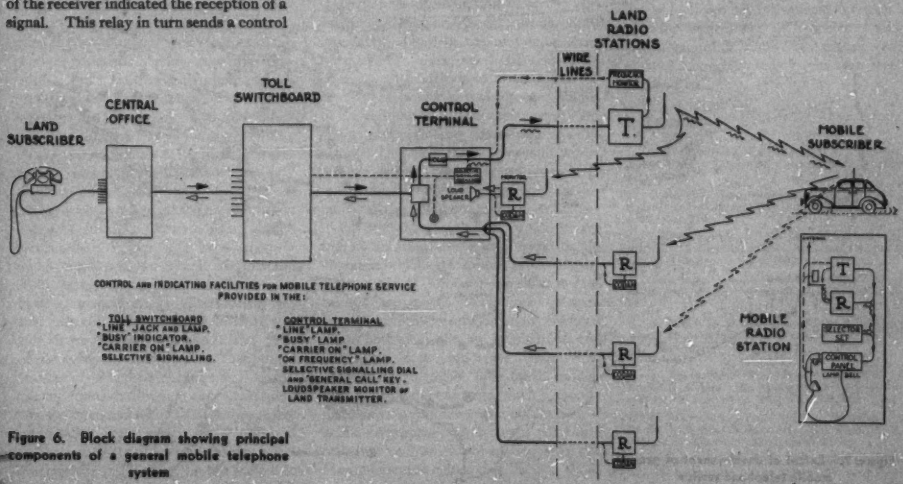


Figure 6. Block diagram showing principal components of a general mobile telephone system

indicates the places in the United States and Canada where one or more such systems were furnishing service at the beginning of 1948. Not shown by the map is the fact that there were then 102 land stations in operation providing service to about 4,000 mobile units.

Coverage

Marginal signals, requiring repetition, are probably less acceptable to common-carrier subscribers than to the users of private mobile systems. The radius of acceptable coverage of an urban mobile system (152–162 megacycle band) is in the order of 15 to 30 miles, depending upon the height of the land transmitting antenna and the terrain and noise level prevailing in the area. By astute selection of land receiver locations based on detailed field surveys, equal coverage areas for both directions of transmission are usually provided. Difficulty may sometimes be encountered in suburban areas where a high noise level is encountered at the extreme range of the land transmitter coverage area; however, this has not so far been a troublesome problem. In general, the minimum acceptable field strength for reception by a mobile unit in a high-noise area is regarded to be about 100 microvolts per meter. In reasonably quiet areas, a field strength of perhaps 30 microvolts per meter is considered to be acceptable. In order to produce a field strength of at least this magnitude out to more than 15 miles, with reasonable antenna heights, a 250-watt transmitter is usually required. Ignition noise, diathermy and other man-made noises are very troublesome in the highway frequency band (30–44 megacycle band). Likewise, long range interfering signals produce annoyance on these frequencies particularly in magnetically disturbed years when high maximum usable frequencies obtain on paths from 1,000 to 3,000 miles in length. Zone allocation of frequencies for exclusive use in areas not over about 1,000 miles in greatest dimension has proved helpful in mitigating this difficulty. Experience has shown that if complete coverage is to be obtained along a highway with land stations not exceeding 250 watts in power the stations will need to be placed every 50 to 100 miles on the average. It is usually desirable to employ land receivers at much closer intervals to make up for the lower powered mobile units.

Multiple Channel Problems

In order to provide service to more than a very few of the automobiles in a large city area, more than a single radio channel must be employed. As the service becomes more commonplace the expansion will doubtless be considerable. Several of the cities in the United States already have three channels in the urban band and one in the highway band in successful operation. Where the frequency difference is considerable, as between urban and highway frequencies, the problems which arise because of the close physical proximity of antennas can usually be cared for by the conventional use of resonant coaxial line filters.

There are, however, situations which arise, as between channels spaced 60 or 120 kc apart in the 152–162 megacycle band which cannot readily be attacked by simple selectivity means. Briefly stated, the primary problems encountered in attempting to operate radio systems closely spaced in frequency and in location are these:

Insufficient receiver selectivity.

Intermodulation between several land transmitters in close proximity.

Intermodulation in non-linear stages of land and mobile receivers.

These problems may best be illustrated by describing the difficulties encountered in a typical multichannel installation in Philadelphia, Pa. A pictorial representation of the Philadelphia case is shown in Figure 8. The Yellow Cab Company radio installation and the first telephone company installation were operating on frequencies which, for purposes of ready reference, are called Channel 5 (152.27 megacycles) and Channel 11 (152.63 megacycles) respectively. These radio systems operated satisfactorily without interference. However, when the telephone company Channel 9 (152.51 mega-

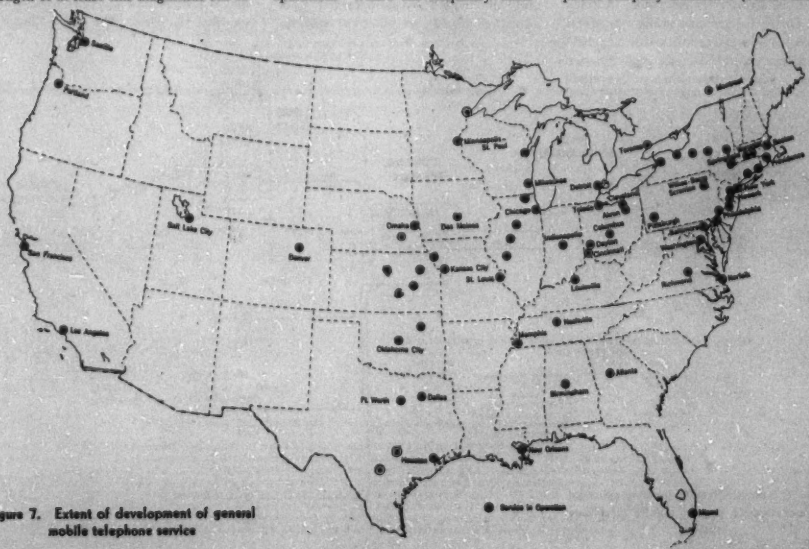
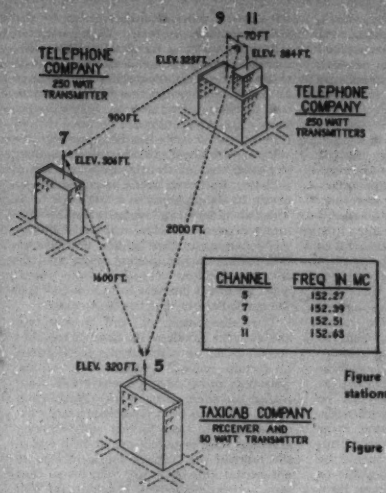


Figure 7. Extent of development of general mobile telephone service



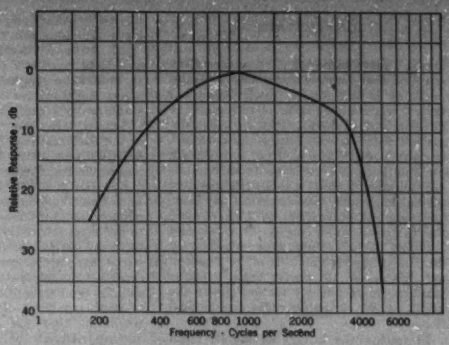


Figure 8 (left). Locations of land stations involved in Philadelphia interference difficulties

Figure 9. Characteristics of frequency weighting network

cycles) installation was placed in operation at a later date, serious interference was noted in the taxicab receivers. Transmitter intermodulation products resulting from the simultaneous operation of Channels 11 and 9 had been anticipated, and a special balancing circuit designed by Bell Telephone Laboratories had been installed to reduce these products to tolerable limits. This balancing circuit, in effect, introduces a voltage from one transmitter into the other in opposite phase and equal in magnitude to the voltage introduced through the mutual coupling of the antennas. The voltages which produce intermodulation are thus canceled. Tests indicated that this circuit was operating satisfactorily, and further investigation disclosed that the interference was caused by serious overloading of the vacuum tubes in the radio-frequency stages of the taxicab company land station receiver.

In the taxi system, the same frequency was used initially for both directions of transmission. Since the land receiver was in the downtown area, it was exposed to strong signals from the telephone company land transmitters. The receiver was also required to be sensitive to weak signals from distant vehicles. The telephone company has avoided this, and a number of other problems, by using one frequency for each direction of transmission. The two frequencies are spaced about five megacycles apart which gives sufficient separation to obtain effective use of selective circuits. This practice has proved to be necessary if serious interference is to be avoided in metropolitan areas, and the taxicab company has now converted its system to 2-frequency operation.

With 2-frequency operation of the taxi system the land station receiver was, of course, no longer subject to overloading and no interference was encountered. However, when the mobile units came in close proximity to the location of the telephone company land transmitters, receiver overloading occurred. Under this condition the combination of strong signals from Channels 9 and 11 produced an interfering signal in the mobile receiver itself. To overcome this interference an automatic gain control can be introduced in the radio-frequency stages of the receiver to reduce the receiver gain when such high-level signals are encountered. This gain control prevents interference when the taxi land station is operating and when the taxi is in the vicinity of the telephone company stations.

A very selective receiver would be required for interference-free operation on channels which are spaced 60 or 120 kc apart. Since frequency-modulated receivers are used, interference between two channels 60 kc apart is not experienced while both channels are operating if the receivers have moderate selectivity. However, a so-called "false busy" indication may be encountered when a mobile user, listening to determine if the channel is idle preparatory to placing a call, hears interference from an operating adjacent channel and concludes that his own channel is in use. This interference is overcome by operating adjacent-channel transmitters simultaneously. The unmodulated signal of the idle channel thus effectively reduces the gain of the mobile receiver and eliminates the interfering signal caused by the busy channel. It is necessary, of course, that all transmitters be in close proximity so that approximately equal signal strengths are received throughout the service area.

Conclusion

The development of mobile radio for telephonic communication with automobiles has extended over almost three decades. Much has been accomplished but much remains for the future. Already the task of providing more and better facilities has introduced new problems for the engineer. The fusion of wire telephone and radio techniques has created a co-ordinated system with great possibilities.

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Discussion

David Talley (International Standard Electric Corporation, New York, N. Y.): In this paper on Mobile Radio, Austin Bailey has done a very commendable job in tracing the developments of the past 25 years in this important field and condensing the more important points into a form suitable for presentation at this meeting. In accomplishing this difficult task, it is felt that possibly several important considerations may have been omitted, other items not fully covered due to limitation on length of the text and probably some points may require additional exemplification. It is the object of my discussion to briefly explore these matters and to possibly further clarify the whole subject of mobile radio communications.

In tracing the history of mobile radio communications developments, Bailey quite properly associates it with the expansion of the police radio systems. Due credit also should be given to E. H. Armstrong's development of frequency modulation (f-m), which together with the advent of suitable vacuum tubes and circuit components for the very high frequencies—30 to 300 megacycles—have made possible the present growth of mobile radio. It may be in order at this point to inject a few statistics to show how quickly radio equipment can be outmoded by advances in the art. In 1940, Federal Communications Commission data listed over 1,250 police radio systems in the United States employing amplitude modulation, mainly in the 1,600–2,000 kilocycle band. Only a few systems were f-m in the 30–44 megacycle range. Today, almost every police system has changed over to f-m operations in the 30–44 or 152–162 megacycle bands, as noted by Bailey. Also, it is significant to observe that power utilities, taxicabs, telephone companies and other users now outnumber the police in the applications of mobile radio, as further discussed later.

The impetus of the recent war to mobile radio is touched upon only too lightly. When it is recalled that every American tank and most other military vehicles were equipped with f-m mobile radio sets operating in the 22–40 megacycle region and that thousands of men became familar with the operation of these mobile sets and the quasi-line-of-sight range of the very high frequency band, one can more readily comprehend the present great interest in the mobile radio services. Wartime research and development, especially in the design of vacuum tubes and components suitable for the very high frequencies, have been instrumental in making it possible at present for manufacturers to supply large quantities of mobile radio equipment at moderate costs.

In his paragraph on Modern Mobile Radiotelephone, Bailey briefly referred to the present trend for mobile radio systems to move from 30–44 to 152–162 megacycles and higher frequencies due to the overcrowded conditions on 30–44 megacycles and because of less noise, shorter automobile antenna heights required for the higher frequencies, and so forth. It should be noted that frequencies in the 30–44 megacycle range will cover greater distances than 152–162 megacycle transmissions where the radio path is over "good soil," that is, earth having high conductivity such as clay, marsh, loam, or swamp land. For transmission over "poor soil" or low conductivity, that is, land consisting largely of rocks, gravel or sand, the maximum communication distances are about the same for both these frequency bands over level terrain.

Shadow losses due to intervening objects in the radio path such as hills, ridges, and buildings are much greater for 152–162 megacycle transmissions than 30–44 megacycles. On the other hand, multiple reflections from buildings in cities are more pronounced on 152–162 megacycles as compared to the lower frequency band. This tends to fill in the deep shadows in the radio path at street level which makes possible uninterrupted communications with moving motor vehicles even along the streets of New York or other large cities. It is necessary, for this condition, that the land station transmitting antenna be located as high as possible with respect to surrounding buildings. In suburban areas, especially in locations where "good soil" conditions exist (as defined before) greater distances can be achieved on the 30–44 megacycle frequencies. This increase in range may amount to as much as 50 per cent under very good conditions. The foregoing factors should be carefully considered when it is possible to have a choice of frequency allocations for a particular installation.

It may be of interest that the writer has talked from mobile radio equipped automobile driving through the streets of Buenos Aires, Argentina to another car in the vicinity of Philadelphia in connection with certain tests made in June, 1947. Telephone calls also have been made from a car of the Mutual Telephone Company in Honolulu, T.H., to different cities in the United States during the past year. The Cuban Telephone Company is installing mobile radio service in Havana and the Compaia Telefónica y Telegráfica Mexicana, an associate of the International Telephone and Telegraph Corporation, is now experimenting with urban mobile service in Mexico City. The international aspect of mobile radio communications is now a well-established fact.

In the section on Typical Present-day Mobile System for Small Areas in the United States, Bailey has very ably described the low-power mobile radio system now usually installed by small police departments, power companies, and taxicab dispatching. The average land station transmitter for these systems generally is rated at less than 50 watts output for operation on the 152–162 megacycle band. Remote control arrangements normally are available so that the equipment can be installed as near the antenna as possible to take advantage of short antenna connecting cable (100 feet or less) thereby reducing the power loss in the antenna transmission line. The remote control unit, which usually comprises loudspeaker, handset, and control keys, ordinarily is installed in the dispatcher's office in the same building. For remote control operations from a more distant point, leased telephone line facilities can be employed.

With reference to the section on Mobile Radiotelephone Equipment, Bailey only mentions and describes the pulse count type of selective signaling equipment (known as the Western Electric Number *106 A* selector set) which is in current use by the Bell Telephone System. This particular selector has a capacity of 2,030 code combinations on a five-digit basis but it is very expensive; costing about $300 each which is an appreciable proportion of the average cost of the mobile radio set in the motor vehicle. A different type of signaling system utilizing tuned reeds has been recently developed which will probably cost less than one-third that of the pulse count selector variety. With four reeds, it is possible to obtain 10,000 numbers on a four-digit basis which should provide adequate numbers for future expansion of the urban and highway mobile services. In addition, the Selecto-Call feature also employing reeds, used by one manufacturer of mobile radio equipment, makes it possible to lock-out all but the particular car that has been called.

The elimination of the push-to-talk switch in the handle of the handset has been accomplished on mobile radio installations in certain railroad pullman cars of the Baltimore and Ohio and Pennsylvania Railroads, on their New York—Washington route. This has been possible primarily because separate transmitting and receiving antennas were installed, one at each end of the railroad car. Space considerations do not permit this on motor vehicles so that some other method is required for full duplex operation as on wire line telephones. This feature, although desirable, is not so important for private mobile systems not connected to the telephone plant.

One other point concerning mobile radio equipment, not mentioned in Bailey's paper, should be of prime interest. This is the high current drain on the motor vehicle's storage battery by present-day mobile equipment. For example, the average 25–30 watt 152–162 megacycle mobile transmitter takes over 40 amperes and the 50 watt 30–44 megacycle transmitter requires as much as 70 amperes from the 6-volt car battery during transmissions. The mobile receiver draws from 7 to 10 or more amperes continuously. It is usually necessary, for optimum service, that the standard battery and charging generator in the car be replaced by heavy duty types. This additional expense may make mobile radio service uneconomical for some private system users. The trend of development by radio manufacturers is towards lower power mobile sets requiring battery currents during transmissions of 20 amperes or less. Mobile units rated at 7–10 watts output power in the 152–162 megacycle band are now available which have low-current requirements, smaller dimensions and yet can cover an appreciable amount of the service area as the higher power equipment. The use of vibrator power supplies instead of dynamotors, availability of improved vacuum tubes and smaller components have made possible the manufacture of these efficient low-power mobile sets. With reduced cost of mobile equipment, it is felt that many more industrial and private users will find it economical to install mobile radio systems, particularly if no additional expenses, such as heavy duty car batteries and generators, are required.

Referring to the paragraph on Control Terminal Equipment, a few more words on the necessity of receiver selection may be in order. When several remote receivers are utilized to cover the service area, the re-

ceiver nearest to the calling mobile station generally affords the best signal-to-noise output to the control terminal. The other remote receivers contribute varying amounts of noise which adversely affect the over-all performance of the circuit. As a result, manual selection of the best receiver has been necessary in the past to exclude the other receivers. With the increasing use of lower power transmitters, additional remote receivers may be needed for adequate coverage purposes. The development of automatic selection circuits for land receivers also will greatly simplify the operation of mobile systems utilizing a number of remote receivers. Since it is not possible to assign a very high frequency channel for the exclusive use of each private mobile radio system operating in a locality, sharing of channels is necessary. It is not uncommon, therefore, for two, three or more taxicab companies in a town or urban areas to operate on the same channel. The resultant interference and obvious operating difficulties may impair the economic value of mobile radio dispatch service to the taxicab industry. One possible expediency pending further technical developments that may afford multichannel operations on a particular frequency allocation, might be to combine taxicab dispatching operations at one central transmitting-receiving station in each locality. With the continued growth of the mobile service, it is probable that subscribers to common carrier mobile telephone service may become the majority of users and a mobile telephone in every automobile is not a too remote possibility.

It is hoped that this discussion of Bailey's excellent paper on mobile radio will serve to further emphasize the many advancements in this important communication service since World War II. Although no one can foretell the future extent or the ultimate form of mobile communications, possibly the following anecdote attributed to Doctor Jewett, the famous scientist and former head of the Bell Telephone Laboratories, may indicate the objective. When Doctor Jewett once was asked to define the ultimate objective of communications, he replied to the effect that when you could take out of your pocket an instrument the size of a watch, dial the number of a particular person anywhere in the world and if he did not answer, you knew he was dead; that is the ultimate of communications.

Ten Years Performance of Lightning-Resistant Wood-Pole Transmission Lines

R. M. SCHAHFER
MEMBER AIEE

W. H. KNUTZ
MEMBER AIEE

OUR INTEREST in improving the lightning performance of transmission lines was accentuated several years ago when we were designing and constructing some additions to our 132-kv steel tower lines.

The service record of these 132-kv lines, compared with the records of similar lines of other utilities in adjoining areas, indicated the need of improvements in our lines. Comparisons were made of construction, type and extent of shielding, exposure, type and number of insulators, use of arcing horns and grading shields, line configuration and spacing, prevailing winds, storm direction severity and frequency, ground resistance values and conductor clearances to towers. All lines studied had reasonably good shielding and were similar in other construction features. The only outstanding difference found was that the ground resistance of our lines was noticeably higher. This comparison, which theoretical calculations also substantiated, indicated that we could improve the performance of our lines by lowering our ground resistance.

Each type of tower was studied as to its minimum flashover clearance, and the resistance of that tower was lowered to a value which was computed to make the tower reasonably free from lightning flashover. Outages from lightning were reduced about 81 per cent.

These results were so encouraging that we decided to incorporate shielding, grounding, and co-ordination of insulation in our 33- and 69-kv wood-pole transmission lines.

Paper 49-126, recommended by the AIEE transmission and distribution committee and approved by the AIEE technical program committee for presentation at the AIEE summer general meeting, Mexico, D. F., Mexico, June 21–25, 1948. Manuscript submitted December 3, 1947; made available for printing March 11, 1948.

R. M. SCHAHFER is mechanical and electrical engineer, W. H. KNUTZ is special engineer, both of the Northern Indiana Public Service Company, Hammond, Ind.

Previous Operating History of 33-Kv Lines

A study was made of the lightning performance of all 33-kv overhead lines of the Northern Indiana Public Service Company for the years 1932 to 1938 inclusive. The year 1932 was chosen because this was the year in which adequate records were available. The year 1938 was chosen because it was the last year prior to our modern lightning-resistant wood-pole transmission line design.

These lines are located in northern Indiana in the area extending from the Illinois state line to the Ohio state line, and from the Michigan state line south about 75 miles. Therefore, a wide geographic area, subject to varied storm hazards is served. Figure 1 shows typical examples of our unimproved lines. Like all systems which have grown up over a period of years, they include many types of construction which have been popular in the decade preceding 1932.

The outages per 100 miles per year for this entire system for the period 1932 to 1938 inclusive are:

Lightning	40.1
Other Outages	31.7

Operating History of Lines with All Wood and Porcelain Insulation

At one time there was a prevailing belief that by means of wood insulation, conductors could be separated sufficiently to prevent lightning flashovers. Prior to 1930, three lines were constructed wherein an attempt was made to insulate the structure so highly that no flashover would occur and that the force of the lightning would be dissipated in attenuation along the transmission line. These three lines traverse some 71 miles of the state of Indiana and are in two different geographic areas which are not subject to the same storms.

The first of these lines, Figure 2, was a 69-kv line, 45 miles long, extending from Plymouth to Goshen, Ind. It was of wishbone configuration using double plank arms. Six insulators were used on the top phase, five on the middle, and four on the bottom phase. Conductors were widely separated. All guys included wood insulators. Care was taken to see that ample clearance was maintained to all distribution circuits. There were no shielding wire or pole grounds. The failure of this line to withstand lightning was so noticeable that every possible weak spot in the insulation such as guy points and distribution attachments were reinforced with greater insulation in two successive investigations, but with no appreciable improvement in operating results. In eight and one-half years, we experienced 217 outages on this line. Lightning caused 163 of these outages, which is 48.3 outages per 100 miles per year. A second 69-kv line, extending from Monticello to Oakdale, Ind., a length of 7.8 miles, was similar in all repects to the previously described line as originally constructed, except that it is in a different geographic area. In 16 years, this line tripped out from lightning 46 times, which is 36.8 outages per 100 miles per year.

The insulation of these two lines was sufficiently high to cause transformer failures in their 69-kv winding even though the transformers were protected by a conventionally located standard lightning arrester. This trouble was corrected by reducing the insulation of the line in a few spans immediately adjacent to each substation.

A third 69-kv line, shown in Figure 3, extended from Oakdale to Lafayette, Ind., a distance of 18.4 miles. Each conductor was supported by five insula-

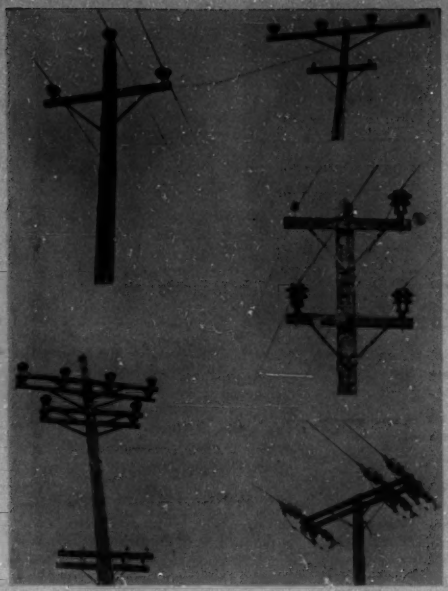


Figure 1 (left). Types of transmission lines constructed prior to 1938

Figure 2. Wood insulated line, 69-kv wish-bone type

tors and was spaced for future 132-kv operation. The line was of *H* frame construction. A small portion of the cross-arm was by-passed by a steel brace, but sufficient wood remained unby-passed to make these structures equivalent in insulating value to the wishbone-type lines previously described. There were no static wire or pole grounds. In 12 years, this line had 218 outages. Lightning caused 125 outages, or 56.7 outages per 100 miles per year.

Summarizing the operation of these three highly insulated lines, none of which employed a static wire, we find that the outages per 100 miles per year are:

Lightning	48.9
All other	29.6

These results are no better and in fact are worse than the record of our entire 33-kv system, and gave little hope that any such type of construction would be successful.

Operating History of 69-Kv Line with Shielding Wire

A 69-kv line, 45.1 miles long, extends from Oakdale to Kentland, Ind. This is in the same geographic area as the last two lines described in the preceding section,—all three lines being connected to the same bus at the Oakdale substation. Hence, all three lines have more or less identical storm exposures.

This line, shown in Figure 4, is constructed on two 10-foot arms using steel braces. Five insulators are used. A shielding wire is supported on a steel bayonet. The shielding wire is grounded at each pole, the grounding-down lead being placed on the surface of the pole. No attempt was made to secure low-ground resistance values, consequently many are higher than they should be. The use of steel braces, steel bayonets, and the position of the down lead greatly reduce the impulse flashover of the line. The performance of this line was much better than the highly insulated lines described previously. The outages per 100 miles per year for the period 1932 to 1947 inclusive are:

Lightning	6.5
All other	21.1

This line is classified as semimodern because it does not take full possible advantage of the wood insulation. Our theoretical studies and our analysis of operating records of the foregoing enabled us to develop the transmission lines we are now building.

Operating History of Modern Design

We classify as modern design those transmission lines which are shielded by an overhead ground wire so located as to provide a cone of protection of approximately 45 degrees. Wherever possible, hardware, which would reduce the value of wood insulation, is eliminated. The shielding wire is grounded at each structure. The grounding down lead is set out from the pole. A high insulating value with all flashover paths co-ordinated is incorporated in designs. We classify also as modern design, those transmission lines which are constructed with high insulation values and without a shielding wire, where such lines are located so that they are shielded by other structures with the same 45 degree cone of protection. Lines constructed parallel to, and under an existing 132-kv tower

Figure 3. Wood insulated, 69-kv *H*-frame line

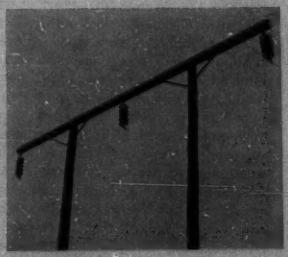


Table I

Circuit	In Service From	In Service To	Years	Miles Last Year	Outages Per 100 Miles Per Year: Lightning	Outages Per 100 Miles Per Year: Other
A	1938	1947	10	23.6	0.8	4.2
B	1939	1947	9	27.9	1.6	1.2
C	1939	1947	8 1/2	45.0	0.8	8.4
D	1940	1947	8	16.2	1.0	4.0
Average					1.0	5.1

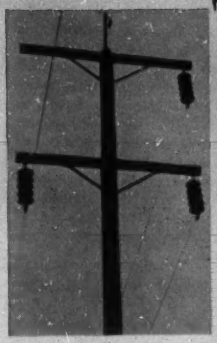


Figure 4. Semimodern 69-kv line

Figure 5 (left center). 69-kv, shielded and grounded construction

Figure 6 (right center). 33-kv, shielded and grounded construction

Figure 7. 69-kv wishbone line, improved by shielding and grounding

line are examples of such type of shielding.

Since 1938, all new and all alterations to existing 33-kv and 69-kv wood-pole lines have incorporated the modern shielding, grounding, and high-impulse insulation-value features.

The first line so built extended from Goshen to Angola, Ind., a distance of 50.8 miles. This line operates at 33-kv. A short section, 0.7 mile long was added at a later date. This line was built with two 10-foot arms and wood braces. One section of this line was built with four suspension insulators for future 69-kv operation.

Another section has two suspension insulators and a third section employs pin-type insulators. Grounds were driven to approximately 5.5 ohms, except for a section about two miles in length where some resistances were over 100 ohms and could not be reasonably reduced. Figures 5 and 6 show some construction features of these lines, later designated as circuits *A* and *B*.

The second line to employ the features of our modern design was the modernization of the previously described wood and porcelain insulated 69-kv line which extended from Plymouth to Goshen. The record of this 45-mile line, previous to modernization, showed 48.3 outages per 100 miles per year from lightning. A shielding wire was installed and grounded at each structure. Maximum ground resistance at each structure was limited to 5.5 ohms. Insulators were rearranged so that the conductors were supported with 4, 5 and 6 units from top to bottom respectively. This rearranging of the insulators improved the impulse insulation of the line and also increased conductor spacing, so that a previously serious dancing conductor condition has been practically eliminated. After modernization, from the years 1939 to 1947, lightning has caused only three outages, which is 0.8 outage per 100 miles per year. This modernized line is shown in Figure 7, later designated as circuit *C*.

A third line was constructed in 1940 using a simplified self-supporting arm design. This line operates at 33 kv. A shielding wire was installed and each structure grounded to about 20 ohms. This type of line will be described in greater detail later. This line is designated as circuit *D*.

The operating record of these four modern lines is shown in Table I.

While many other lines have been built or altered to embody our modern design features, their operating record is not included in the foregoing summary. The lines included are modern from terminal to terminal, and no guessing or estimating is necessary to analyze their performance. At the time this analysis was started, all other lines were intermingled with some sections of older designs. Hence, to include them in this summary would involve some estimating and create a question as to whether operations were being included or excluded fairly.

Geographic Location of Lines Investigated

Figure 8 shows the location of the various lines discussed previously. Thus, it will be readily apparent that the lines cover a wide geographic area, across the state of Indiana, and are not subject to any restricted storm exposure.

Modern Design Features

The 69-kv wishbone line previously described employed double plank arms and special hardware. The arms were difficult to install, mechanical safety factors exceeded requirements, and costs were high. The 69-kv semimodern line did not take full advantage of the wood insulation. Both this and our first modern lines have an unbalanced load on one crossarm. The wishbone line does result in better mechanical balance. As a result of these comparisons, a simplified self-braced arm structure was developed. This is known as our type *F* single circuit construction. This structure is shown in Figure 9. The

Table II

Operating Summary of Various Types of Construction

Line Type	Year Miles	Miles Last Year	Years Service Inclusive	Outages Per 100 Circuit Miles Per Year: Lightning	Other	Total
33-kv*, all types	4,719.1	674.3	1932 to 1938	40.1	31.7	71.8
Wood insulation	683.1	71.2	1932 to 1947	48.9	29.6	78.5
Semi modern	721.6	45.1	1932 to 1947	6.5	21.1	27.6
Modern design	968.6	112.7	1938 to 1947	1.0	5.1	6.1

* Nonlightning resistant except that about 5.6 per cent were shielded.

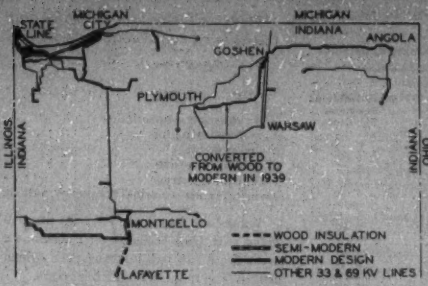


Figure 8. Location of 33-kv and 69-kv wood-pole transmission lines

INSULATION FLASHOVER VALUES — IMPULSE VOLTAGE - KV												
KIND OF INSULATION	TOP CONDUCTOR				MID. CONDUCTOR				BOT. CONDUCTOR			
	UNIT	FLASHOVER PATH BY			UNIT	FLASHOVER PATH BY			UNIT	FLASHOVER PATH BY		
	U E	AIR	PORC ARM POLE	PORC AIR	U E	AIR	PORC ARM POLE	PORC AIR	U E	AIR	PORC ARM POLE	PORC AIR
ARM	36	-	410	-	63	-	800	-	45	-	520	-
	47	-	-	575	51	-	-	630	55	-	-	690
POLE	58	-	665	-	58	-	665	-	97	-	1100	-
SUBTOT.	-	-	1075	575	-	-	1465	630	-	-	1620	690
RAIN 70%	-	-	732	402	-	-	1025	441	-	-	1134	483
PORC.	45KV	-	220	220	45KV	-	220	220	45KV	-	220	220
AIR GAP	46	980	-	-	51.5	1070	-	-	58	1210	-	-
	36	-	-	710	36	-	-	710	36	-	-	710
TOTAL	-	960	972	1332	-	1070	1245	1371	-	1210	1354	1413

REQUIRED GROUND RESISTANCE					
$1\frac{1}{2} \times 40$ WAVE		ITEM	CONDUCTOR		
$f = 2.1\mu$	GROUND		TOP	MID.	BOT.
$n = 1$	COND	S, FEET	3.71	4.54	8.05
$T = 0.275\mu$		H, FEET	33.58	33.58	33.58
$t = 3.5\mu$	H	B = 2H - S	63.45	62.62	58.66
$\alpha = 0.9$		B ÷ S	17.1	13.8	6.9
$I = 100{,}000$	GRADE	Cn	0.34	0.32	0.24
40 FT. POLE	B	(1 - Cn)	0.66	0.68	0.76
$e = \frac{34.5}{\sqrt{3}}\sqrt{2}$	H	E, MIN.	960	1070	1210
		e_1	28	28	28
	IMAGE	$R = \frac{E - e_1}{(1-Cn)\alpha I}$	15.7	15.4	17.3

Figure 11 (right). Type *F*, single circuit, 33 kv

Calculation of insulation level and required ground resistance

dimensions are given in Figure 10. Arms are offset from center and coupled together at one end by means of an inexpensive U plate. This results in a more equal arm loading and eliminates the distortion frequently encountered when an arm supports a conductor on one end and none on the other. While this construction results in less clearance to the down-lead ground wire of the top conductor than for others, a study of the impulse insulation value of the various conductors, when corrected for the effect of coupling factor with their relationship to the shielding wire, will show this to be a well balanced structure electrically as well as mechanically.

The shielding wire shields all conductors in a cone of protection of approximately 45 degrees or less. This value was obtained as a good compromise considering the operating record expected and obtained, the cost of greater or less length of pole, and the amount of pole needed above the crossarm so that this path of flashover would not weaken the impulse value of the structure. The shielding wire is grounded at each pole. The down lead is extended out from the pole by means of extended neutral stud bolts, and is offset transversely from the pole center line to equalize the flashover values of the three phases. Steel stud bolts are used rather than wood extensions because of permanency, but being steel, must be located so as not to reduce the insulating value of the structure. The location of one extended stud bolt at the shielding wire attachment also supports the weight of the down lead and thus minimizes the probability of establishing a fatigue point in the shielding wire. If the down lead wire were to lay on the surface of the pole and thus eliminate all value of the wood insulation of the pole, then the impulse strength of the entire structure would be reduced to about one-half of the structure we are now using. From published data on the expectancy of lightning strokes to exceed any given value, we can predict that a structure designed to protect

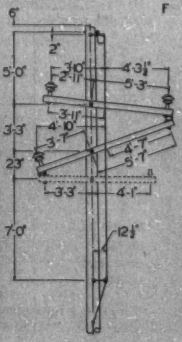
Figure 9 (left). Type *F*

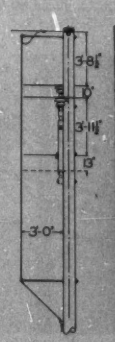
Figure 10. Type *F*

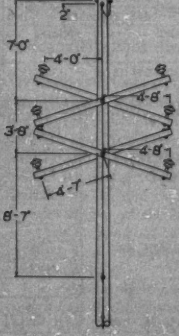
Single circuit 33-kv dimensions

Figure 12 (left). Type *F2*, double circuit, 33 kv

Figure 13. Type *F2*, double circuit, 33 kv dimensions







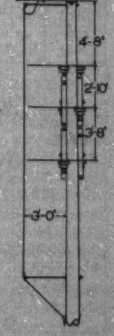


Table III

33-Kv and 69-Kv System Outages and Per Cent of System Made Lightning Resistant, By Years

Year	Total Miles Of Line	Outages Per 100 Circuit Miles Per Year: Lightning	Other	Total	Miles Lightning Resistant, Per Cent
1932	804	32.4	28.1	60.5	5.62
1933	799	35.9	30.7	66.6	5.65
1934	799	44.6	32.5	77.2	5.65
1935	801	29.3	25.4	54.7	5.63
1936	789	46.6	38.5	85.1	5.72
1937	789	38.4	31.6	70.0	5.72
1938	813	38.1	25.9	64.0	7.68
1939	838	29.1	25.7	54.8	18.05
1940	805	21.5	38.0	59.5	23.20
1941	815	19.1	23.1	42.4	29.00
1942	811	38.7	37.0	75.7	33.20
1943	821	29.6	28.5	58.1	35.80
1944	811	25.1	30.1	55.2	37.30
1945	810	23.9	27.6	51.5	38.00
1946	826	24.1	28.9	53.0	43.40
1947	828	27.6	58.4	86.0	44.80

against lightning strokes of 100,000 amperes will have only about one-sixth of the lightning flashovers that would occur on a structure designed to protect against 50,000 amperes.

It is to be noted that provision is made for distribution underbuild. When the distribution is single phase the arm which supports it is coupled to the lower transmission arm by means of another U plate. For 3-phase underbuild an additional 2-pin arm is installed below the arm supporting the single distribution conductor. The shielding wire is used as a neutral for the 12.5-kv or 4-kv distribution system.

This simplified structure incorporates all the lightning resistant features of shielded and grounded lines constructed with two arms and wood braces. The method of determining flashover values and desired ground resistance is illustrated in Figure 11. The air gaps are from conductors to grounded down lead or shielding wire by one of three paths.

1. Direct from conductor to ground wire or down lead wire by air,
2. Across porcelain, arm, and pole to ground wire, or
3. Across porcelain, arm, and by air to the down lead wire.

Flashover values are calculated from the following equation by L. V. Bewley.[1]

$$V=(1-C_n)aR'I+e$$

Where

V = voltage across insulation
C_n = coupling factor
a = crest factor
R' = effective resistance
I = lightning current
e = instantaneous normal frequency voltage

Values are based on $1^1/_2\times40$ positive wave with time to flashover t taken at a wave front f of two microseconds. Span length T is reduced to microseconds.

It will be seen that our design provides a flashover path directly from conductor to down lead wire less than the other paths. This is desirable so that any flashover which may occur will follow this path, and arm and pole splintering will thus be avoided. Although the insulation of each conductor is different, each when related to coupling factor, which takes in account the spacing of the conductor relative to the shielding wire, requires about the same ground resistance for the same lightning protection. In calculating the flashover values of the wood paths, we reduced values of wood 30 per cent to allow for the effect of rain.

A 2-circuit structure similar to the single circuit structure is shown in Figure 12. This is known as our Type *F2* double circuit construction. The dimensions are given in Figure 13. For this structure the arms are centered on the pole. Two coupled arms are installed on one side of the pole and two on the other side. When double arms are needed, it is necessary to revert to a structure with three horizontal double arms. The flashover values and desired ground resistance

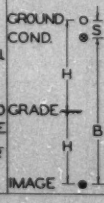
INSULATION FLASHOVER VALUES

IMPULSE VOLTAGE - KV

KIND OF INSULATION	TOP CONDUCTOR: UNIT VALUE	FLASHOVER PATH BY: AIR	PORC ARM POLE	PORC ARM AIR	MID CONDUCTOR: UNIT VALUE	FLASHOVER PATH BY: AIR	PORC ARM POLE	PORC ARM AIR	BOT CONDUCTOR: UNIT VALUE	FLASHOVER PATH BY: AIR	PORC ARM POLE	PORC ARM AIR
ARM	55	-	730	-	55	-	730	-	55	-	730	-
	55	-	-	730	55	-	-	730	55	-	-	730
POLE	82	-	985	-	82	-	985	-	103	-	1230	-
SUBTOT.	-	-	1715	730	-	-	1715	730	-	-	1960	730
RAIN 75%	-	-	1286	547	-	-	1286	547	-	-	1470	547
PORC.	45KV	-	245	245	45KV	-	245	245	45KV	-	245	245
AIR GAP	48	1120	-	-	56	1300	-	-	56	1300	-	-
	36	-	-	780	36	-	-	780	36	-	-	780
TOTAL	-	1120	1531	1572	-	1300	1531	1572	-	1300	1715	1572

REQUIRED GROUND RESISTANCE

$1\frac{1}{2}\times40$ WAVE
$f = 2\,\mu$
$n = 1$
$T = 0.185\,\mu$
$t = 2.5\,\mu$
$\alpha = 0.78$
$I = 100{,}000$
45 FT POLE
$e = \frac{34.5}{\sqrt{3}}\sqrt{2}$



ITEM	CONDUCTOR: TOP	MID.	BOT.
S, FEET	4.67	7.50	11.17
H, FEET	38.08	38.08	38.08
B = 2H - S	71.49	68.66	64.99
B ÷ S	15.3	9.2	5.8
Cn	0.33	0.28	0.22
(1 - Cn)	0.67	0.72	0.78
E, MIN.	1120	1300	1300
e_1	28	28	28
$R=\frac{E-e_1}{(1-C_n)\alpha I}$	20.9	22.6	20.9

Figure 14 (left). Type *F2*, double circuit, 33 kv

Calculation of insulation level and required ground resistance

Figure 15. Type *E*

Double circuit 69 kv and 12.5 kv

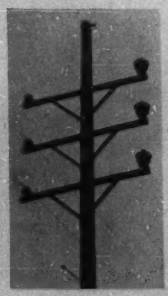
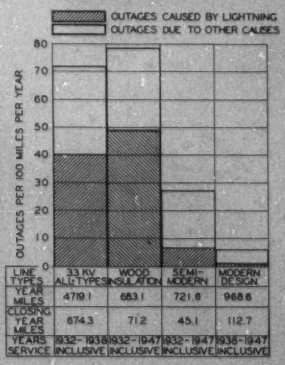


Figure 16. Lightning outages by type of construction



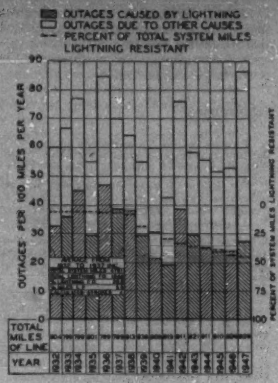


Figure 17. Lightning outages by years

33-kv and 69-kv system

values are shown in Figure 14. Impulse flashover values of this structure are higher than in the single-circuit structure because the flashover paths are longer. Because of structural strength, the economic span length of 2-circuit pole lines is shorter than for single-circuit pole lines. The shorter spans increase the effective impulse flashover values. Thus for the same lightning current protection level allowable ground resistance values become higher than for the single-circuit design.

Figure 15 shows a lightning resistant structure with 69-kv clamp top pin insulators on one side of the pole and a 12.5-kv circuit on the other side.

Outage Record

The performance record of the four types of wood-pole transmission lines discussed previously, classified in types of construction, is summarized in Table II.

Figure 16 shows outages classified by types of construction graphically. Wood-insulated lines without shielding and grounding had a less satisfactory record than lines with no attempt made to protect against lightning. The line with fair shielding, inadequate grounding, and low flashover values due to steel bayonets and steel braces had a record of few lightning outages. The lightning outages on lines of modern design approach zero.

It is interesting to note that as the designs for protection against lightning improved, outages due to other causes were reduced materially. This may be due to:

1. Modern lines have greater conductor spacing than most older lines. This should reduce number of phase-to-phase short circuits.

2. Modern lines are designed to minimize the possibility of dancing conductors getting together by maintaining a minimum relationship of conductor sag to conductor arm spacing.

3. Modern lines are relatively new and designed to provide a greater safety factor.

4. Modern lines are relatively new and still have better tree clearance.

System Operating Improvement

Construction of scientifically designed transmission lines of adequate lightning-resistant quality should result in a predictable decrease in lightning flashover. The correctness of this theory should be demonstrated by a decrease in lightning outages in our operating record.

An analysis of all lightning outages on 33- and 69-kv wood-pole lines was computed for each year from 1932 to 1947 inclusive. The per cent miles of lightning-resistant lines for each year for the same period was calculated also. The results of the calculations are shown in Table III.

Composite graphs were then plotted so that 100 per cent of the lines made lightning resistant would correspond to zero lightning outages; and 5.6 per cent, which was approximately the per cent shielded and grounded lines prior to 1938, would correspond to the average outages for the period prior to 1938. The composite graph is shown in Figure 17. Figure 17 shows that since 1932 the per cent miles of lightning-resistant lines has increased from about five per cent to over 40 per cent and lightning outages have decreased about one-third, in other words, flashovers from lightning are decreasing directly in proportion to the per cent of lines we have improved.

A trend curve often portrays results of a change better than a year-by-year curve. Storm frequency and severity varies from year to year. Our experience with lightning-resistant lines extends over a 10-year period, and over various parts of the state. A 6-year trend curve, Figure 18, was plotted from the same data given in Table III. This also shows a close and direct relationship between the per cent reduction in outages caused by lightning and the per cent of the system constructed on a lightning-resistant basis.

Figure 18. Lightning outage trend

33-kv and 69-kv system

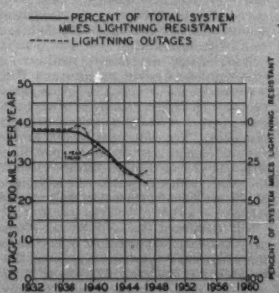


Figure 19 (above). Overhead ground wire burned by lightning

Figure 20 (right). Overhead ground wire burned by lightning

Ground Wire Burn Downs

For shielding wires we use a small high-strength composite strand with equivalent current capacity of number eight copper. In a few cases we have used number two aluminum cable steel reinforced or number four solid copper. Strength and current capacity have been thought ample for the service required. However, we have had two cases wherein lightning had damaged small shielding conductors.

In one case, a number eight 3-stranded copper-steel shielding wire was burned in two. The burned conductor ends are

shown in Figure 19. This was found just after a lightning storm. A witness stated that lightning struck at the location of the burned shielding wire. A very rigid inspection of the power conductors of both the transmission circuit and its underbuilt distribution circuit failed to disclose any evidence of power arcs. There was no record of any circuit breaker operation on either circuit which could possibly be involved in the burn down. There is no reason to believe that there was 60-cycle current contributing to this burn down of the shielding wire.

In another case, a number two aluminum cable steel reinforced shielding wire was found after a severe lightning storm with all aluminum strands burned through, the steel being undamaged. This conductor is shown in Figure 20. Construction of this transmission circuit had just been completed and the circuit was not energized until after the mentioned damage occurred. A witness stated that there was a lightning stroke at the location of the conductor burn down. There seems to be no possible way for 60-cycle current to have contributed to this damage. To date, the limited number of such occurrences in the year-miles of our experience are so few that we do not feel justified in installing any larger conductor to prevent such damage.

Conclusions

1. Full advantage of the impulse insulation strength can be obtained by inexpensive methods.

2. Due to the shape of the lightning severity curve, flashovers from lightning decrease more rapidly than the ratio of increased structure impulse insulation strength.

3. Ground resistance should be determined by the protection desired and the minimum impulse insulation of any of the conductors by any of its possible flashover paths.

4. It is desirable that the minimum flashover path be directly from conductor to down lead wire through air and not over wood in order that splintering of wood will be avoided.

5. Small, high-strength, shielding wires result in inexpensive construction. The few cases of trouble to date do not justify larger conductors and more expensive construction. This item should be reviewed from time to time to be certain that the present opinion is not based on insufficient data.

6. It is expected that ultimately our transmission system will be practically immune to lightning. Outage expectancy will be of the order of one outage per 100 miles per year, and few of these will cause damage.

7. No application of pure engineering theory has produced results more gratifying than to see the reduction of lightning outages of our entire system almost directly in proportion to our prediction.

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Discussion

Opie D. Evans (Oklahoma Gas and Electric Company, Oklahoma City, Okla.): The authors are to be commended for providing the industry with their experience in the design and operation of lightning-resistant wood pole transmission lines. Experience is still our best teacher, and all of us interested in this subject are indebted to the authors for their efforts in providing us with the information contained in this paper.

At the beginning of the paper, my attention was directed to the large number of outages attributed to causes other than lightning. For example, on the 45.1 miles of 69-kv line shown in Figure 4, outages listed as due to other causes are 21.1 per 100 miles per year as against only 6.5 outages per 100 miles per year due to lightning. Trip-outs due to causes other than lightning on the 66-kv system of the Oklahoma Gas and Electric Company are less than 25 per cent of those caused by lightning. It would be interesting to know if some of the outages, listed as due to causes other than lightning, were caused by sleet and glaze storms.

The authors state that, on two 69-kv lines on which there was wood insulation without shielding wires, the insulation of the line was so high as to cause failure of the substation transformers, even though lightning arresters were used to protect the transformers. They further state that this trouble was corrected by reducing the line insulation in the spans adjacent to the substation. If this lowering of the line insulation did correct this trouble, then it certainly resulted in more line trip-outs. This does not seem to be the proper approach to this problem. Modern lightning arresters should protect the transformers from all such disturbances if the breakdown values of the transformers and lightning arresters are co-ordinated properly and if the arresters are properly applied. The purpose of the arresters is to protect the substation from such disturbances rather than have the lowered insulation of the line protect the arresters and transformers.

It is interesting to note in Figure 7, that a different number of insulators is used for the various phases. This would increase the conductor spacing which, I gather, was the primary purpose of this type of construction. It is not believed that the variation of the coupling factor between the shield wires and the three phases would justify the use of such design as standard.

Good design necessitates that careful consideration be given to glaze and sleet storm protection as well as to lightning protection. This is particularly so in our territory where sleet and glaze storms are quite prevalent. The clearances shown in the authors' designs for the single pole lines would never be acceptable in our section of the country unless the spans were very short, in which case an uneconomical design would result.

The Oklahoma Gas and Electric Company has under construction, or in the design stage, approximately 150 miles of 138-kv wood pole line and 125 miles of 69-kv wood pole line. It has been our experience that, when both lightning and sleet storms are considered, the *H*-frame type of construction provides the best and most economical design, and most of the foregoing lines are of this type of construction. Two shield wires are used with a shielding angle of 30 degrees or less. Adequate midspan clearances are provided for lightning and "dancing" conductor protection.

In some cases, due to rights-of-way restrictions or for other reasons, it becomes necessary to use single pole design. In our single pole design we use construction similar to Figure 4 of the subject paper. A 10-foot wood arm with wood brace is used for the top conductor and a 12-foot wood arm with wood brace is used for the two lower conductors. A shielding angle of 30 degrees or less is maintained for lightning protection. A 5/8 inch round steel auxiliary crossarm brace rod is used in this design to overcome the unbalanced load on the top crossarm. This rod is bent to shape and threaded at both ends. One end of this rod is attached to the unloaded end of the top crossarm and the other end is inserted in the pole five feet below the top crossarm. Vertical clearances between conductors, and between conductors and shield wire, are at least as much as 125 per cent of the heavy loaded sag plus six inches. Spans around 350 feet are used when possible, and adequate midspan clearances are provided for lightning protection.

For our transmission lines, no particular design is used to obtain a low-ground resistance other than coiling the wire around the butt of the pole and using a galvanized

pole ground plate on the bottom of the pole. A 1-bolt guy clamp is inserted in the pole ground wire about 8 feet and 6 inches above the ground to provide an easy way of disconnecting the pole lead from the shield wire to measure the pole ground resistance. Should we experience undue trip-outs due to lightning, we intend to measure the pole ground resistances and install ground rods or counterpoises in those places where ground resistances are excessive. One 69-kv line (5.55 miles of *H*-frame construction and 11.38 miles of single pole construction) using this grounding scheme was constructed in 1937. Ground resistances were measured one year later and 30.9 per cent of the pole grounds measured 20 ohms or less and 64.5 per cent of the pole grounds were 50 ohms or less. Calculations show that we can expect less than six outages per 100 miles per year with our single pole design (using five 10-inch diameter, 5¾-inch spacing insulators) if the pole ground resistances are 50 ohms or less.

The possibility of offsetting the pole ground wire from the pole to obtain advantage of the pole insulation is very intriguing. It certainly is desirable that the minimum flashover path be directly from the conductor through air to the down lead, to prevent splintering of the wood. Such co-ordination may be difficult to obtain, however, with suspension insulators under all conditions. The resulting windstorms which accompany many of the rains in this section of the country could reverse the co-ordination on one or two of the phases. The use of stud bolts to offset the down lead is a novel idea and one well worthy of consideration. We have often considered offsetting the pole down lead but we were afraid that by attaching the pole ground lead to the shield wire, a fatigue point and possible future source of trouble would be established.

In Figures 9 and 10, the offset lead is shown as brought down on the crossarm side of the pole whereas in Figures 5 and 6 the offset down lead is shown on the back of the pole. From an operation and safety standpoint, the placing of the offset lead on the back side of the pole (Figures 5 and 6) seems preferable since it provides better clearances for replacing arms and braces on an energized line.

G. E. Dean (Public Service Electric and Gas Company, Newark, N. J.): On January 1, 1948, the Public Service Electric and Gas company had 693.8 circuit miles of overhead open wire 26-kv transmission lines. Of this amount approximately 15 circuit miles of lines were erected on steel towers leaving 679 circuit miles on wood poles. For 1947 the over-all performance of this 26-kv wood pole transmission system was 12.0 lightning outages per 100 miles per year compared to 38.2 before protection was installed. This total of 679 circuit miles of 26-kv wood pole construction was composed of 524 circuit miles of lightning protected circuit and 155 circuit miles of unprotected wood pole construction. In other words, 77.2 per cent of all our wood pole 26-kv transmission lines were of lightning-protected construction.

For a number of years complete operating data on the performance of lightning protection for wood pole 26-kv transmission was kept for comparison with unprotected lines. These data indicated that 85 per cent of all lightning interruptions were avoided by the use of the elevated protected phase wire scheme. Actual values of lightning trip-outs per hundred circuit miles per year for the five year period were 38.2 for the unprotected lines and 5.8 for the protected lines. Referring to the six year trend curve in Figure 16 for the condition where 38 per cent of all transmission has received lightning protection, the elevated protected phase wire scheme used by this company would give 25.9 lightning outages per hundred miles per year as compared to 27.5 for ground wire protection.

Because of this record of reduction of line outages caused by lightning, additional lines will be reconstructed to provide the protection, all new lines or portions of lines, however small, are built with the lightning protected type of construction and all individual pole replacements in unprotected lines are made with adequate pole height and with crossarm details appropriate for protection. These policies will be continued so that ultimately all lines will be protected.

The lightning protection scheme used on these 26-kv wood pole lines is based upon using 1-phase conductor as a combination ground wire and phase wire. In detail, for single circuit pole lines one phase is elevated above the other two by placing it upon a pole top or ridge pin. This elevated phase conductor is then provided with lightning flashover protector tubes to drain off lightning discharges and to cut off any power follow-up current at the first current zero. For double circuit pole lines three crossarms are used and the two top phase conductors are provided with flashover protector tubes. It is not necessary to equip the elevated phase conductors with tubes at each pole and the distance between protected poles varies with the resistance of the ground connection of the tubes. For single circuit construction, the distance between protected poles reaches 1,000 feet for 10-ohm ground resistances, and for double circuit poles the distance between protected poles reaches 950 feet for 10-ohm ground resistances.

With this background, it is difficult to justify the installation of overhead ground wires. In fact, there is more to be considered than the higher cost of the ground wire protection since this company experienced so much mechanical trouble with ground wires on wood pole transmission lines that their use was discontinued some years ago. Large Copperweld ground wires were used for these ground wires under discussion.

It is further felt that the use of small size ground wires may be difficult to justify because of the existence of data which show that ground wires under high mechanical loads may be burned down by lightning currents which would not fuse similar wires under no mechanical loading.

R. M. Schahfer and W. H. Knutz: Evans calls attention to the ratio of outages from causes other than lightning to those caused by lightning on the line shown in Figure 4, this ratio being 3.25 to 1; compared to a ratio of 0.25 to 1 for his 66-kv system.

In making the foregoing comparisons at least two general considerations should be observed.

1. The method of tabulating line outages and what is included.

2. When comparing to lightning, the lightning resistant characteristics of the lines under consideration.

Our method of recording line outages accounts for each time service on the line is interrupted. Therefore, we include as outages interruptions to service caused by the public, line and substation equipment failure, relay errors and inadequacy, operating errors, switching surges, overloads and emergency transfer of power.

The 16 year record of outages per 100 miles per year for the line shown in Figure 4 has been itemized as shown in Table I.

Of the total 27.6 outages per 100 miles per year 7.0 were caused by a very high wind displacing conductors so that contact was made with the steel arm braces. This wind lasted several hours and considerable damage resulted throughout northern Indiana and Illinois.

Line performance may be compared in several ways, one interesting method is to take the ratio of other than lightning outages of a particular line and compare lightning outages of this and other lines to this value. Using other than lightning outages experienced on the line shown in Figure 4 as a basis of comparison, we obtain the ratios given in Table II.

We agree that good design must give consideration to sleet and wind as well as to lightning protection. To these factors we add several clearance conditions and economics. However, our paper was confined to the performance of lines with regard to lightning. We note that Evans uses a minimum vertical conductor clearance of 125 per cent of heavy loading sag, plus six inches. We experience little trouble with lines using pin-type insulators which have span length, cross arm spacing and conductor sag so co-ordinated as to have vertical conductor separation equal to the sag of the upper conductor plus 20 per cent of the sag of the lower conductor, at 32 degrees Fahrenheit, with 1/2 inch radial ice. For suspension insulators 75 per cent is used. This method of determining vertical conductor separation provides for different types of conductors being used on the same structure.

Because of the wide separation of arms necessary to provide vertical conductor clearance when suspension insulators are used, and due to the difficulty, mentioned by Evans, in co-ordinating insulation when insulators are displaced by wind, we are giving serious consideration to the use of clamp top pin type insulators on 69-kv lines. When suspension insulators are used, we balance insulation against ground resistance on the assumption that conductors will be displaced toward the grounded down lead by 30 degrees.

The grounded down leads on our first lightning resistant lines were installed on the side of the pole away from the arms. The impulse strength of the pole structure is not changed if the down lead is on the arm or opposite side of the pole. These two locations were discussed at great length in our various safety groups, opinions being closely divided. Those in favor of locating the down lead on the arm side finally won. No fundamental principles of design being involved, we made the change and are installing the down lead on the arm side of the pole.

Table I. Outages Per 100 Miles Per Year on the Semi-modern 69-Kv, 45.1 Mile Line, from 1932 to 1947, Inclusive

Wind	7.0	Equipment failure	3.1
Lightning	6.5	Trees	1.5
Unusual operations	4.7	Unknown	1.0
Dancing conductors	3.1	Caused by public	0.4
Birds and animals	0.3		

No trouble has been encountered to date with fatigue at the point where the down lead is attached to the overhead ground wire, the attachments being made by using a very slack loop. This is shown in Figures 10 and 12. A detail standard is issued to our construction forces regarding this attachment.

Dean calls attention to mechanical troubles experienced with overhead ground wires. Our company, like many others, has had sad experiences in burned down overhead ground wires and we followed the general practice of removing them. These troubles occurred on old type construction. Overhead steel ground wires were often attached by means of U bolts to steel bayonets. Steel braces were used on crossarms, often the arms were short. Grounded down leads were attached directly to the pole and grounded about every fifth pole, with ground resistance usually high. Some of the overhead ground wires were steel and rust weakened them, some were copper covered steel and the copper peeled off. Consequently, grounding installations on these lines not only lacked mechanical stability but contributed toward low structural flashover values.

Practically every lightning discharge would result in a flashover. Due to low insulation values, flashover consisted of both lightning current and 60-cycle current. Engineers could not determine which caused the damage.

Our ten year experience justifies the use of small size, high strength, corrosion resistant overhead ground wire as to both economics and reliability. There is no comparison between the old type lines and the present lines which are balanced mechanically and electrically. Our modern lines, described in the paper, have given very satisfactory service. The only two burn downs of overhead ground wires experienced in ten years is described in the paper.

We do not believe these two cases of burn downs are sufficient to warrant the expense of adopting larger size overhead ground wires. Larger wires increase structure loading, require more poles and pole fixtures with resultant increase in investment and maintenance charges.

Table II

Type of Line	Outages per 100 Miles per Year: Other	Outages per 100 Miles per Year: Lightning	Ratio of Other Outages on Semi-modern Line, to Lightning Outages of Same and Other Type Lines
Semi-modern	21.1	6.5	21.1 to 6.5 = 3.25 to 1
All 1932–1938	31.7	40.1	21.1 to 40.1 = 0.53 to 1
Wood-porcelain insulated	29.6	48.9	21.1 to 48.9 = 0.43 to 1
Modern	5.1	1.0	21.1 to 1.0 = 21.1 to 1

We have a section of 33-kv line, installed in 1938, which is particularly exposed to lightning. The ground is higher than surrounding areas and structure resistance somewhat above other sections. Magnetic links have been installed in this area and measurements of some severe strokes have been obtained. No flashovers resulted.

About a year ago we lowered about a mile of the overhead ground wire on this line and several engineers and several construction men closely inspected the entire length. No evidence of damage was found.

A New 150-Kilowatt Transmitter for Standard Band Broadcasting

T. J. BOERNER
NONMEMBER AIEE

RADIO BROADCASTING service in most densely populated centers of the world is now handled economically by a plurality of medium power transmitters located at strategic points adjacent to the population centers. High-power radio broadcast service employing medium frequency, amplitude modulated transmitters provides an unexcelled, economical vehicle for education and entertainment of large population groups in sparsely settled areas. The modern 150-kw transmitter described herein was designed to meet the need for service in these areas. Fundamentals were established in 1945 and active product design started early in 1946. Design considerations and performance specifications are presented along with illustrations and performance data on the final product.

Basic Design

The prime design objective was an efficient, compact transmitter capable of delivering fine performance economically. New design features reflect full cognizance of prior art on medium frequency broadcast transmitters. Circuits, tubes and components were investigated.

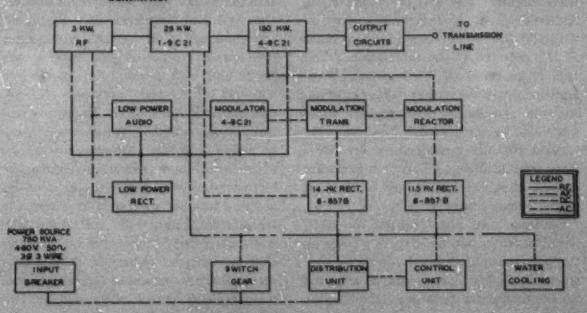
A carrier frequency range of 540 to 1,600-kc was chosen as the band in greatest use internationally for medium frequency broadcasting. The high-level system wherein a class *C* final amplifier is amplitude modulated by a class *B* modulator was the final circuit choice, after a study of high efficiency systems to select the most advantageous circuits for 150-kw carrier output. The highest over-all efficiency of the high level system along with the relative ease of adjustment outweighs the disadvantages of such factors as additional tubes and large size modulator components. Lowest total power input results in power savings which would otherwise be a major operating cost factor at many of the locations where the transmitter would be installed.

An operational advantage of the high-level system is the ease with which the transmitter can be adjusted by operating personnel of limited skill to maintain high quality performance. This advantage is due to the fact that a moderately economical tube complement provides the required performance without the use of over-all feedback which normally calls for critical radio frequency circuit adjustment. The final radio frequency power amplifier in the high-level system is driven well beyond saturation thus swamping hum generated by a-c filament heating as well as providing ample grid swing for modulation peaks.

The disadvantage of higher initial tube cost in the high level system is not an important factor in over-all economy because operating conditions are conservative thus resulting in exceptionally long tube life. Size of modulation transformer and reactor are not an imposing problem because circuit advances and transformer design techniques learned in recent years have resulted in reduced size and lower cost of these components. Combined weight of transformer and reactor in recent 50-kw high-level transmitters is 9,700 pounds as compared to 20,000 pounds in a transmitter of the same rating designed in 1938.

The *9C21* water cooled tube was chosen for use in the final amplifier of the audio and the radio frequency systems. Prior experience with the *9C22* tube, the air-cooled counterpart of the *9C21*, for power amplifier and modulator service in 50-kw high-level transmitters has been entirely satisfactory. Average life at the first installation using this tube type has passed the 10,000 hour mark. Some higher power tube types are in existence but operating experience did not appear sufficient to warrant their consideration.

Figure 1. Block diagram, 150-kw broadcast transmitter



Paper 48-132, recommended by the AIEE communications committee and approved by the AIEE technical program committee for presentation at the AIEE summer general meeting, Mexico, D. F., Mexico, June 21-25, 1948. Manuscript submitted March 1, 1948; made available for printing March 17, 1948.

T. J. BOERNER is with the Radio Corporation of America, Camden, N. J.

Engineers who contributed to the design were J. C. Walter and staff on the over-all system, N. J. Oman on audio-frequency circuits and T. N. Newman on mechanical features.

This paper also was presented at a meeting of the AIEE New York (N. Y.) Section, April 23, 1948.

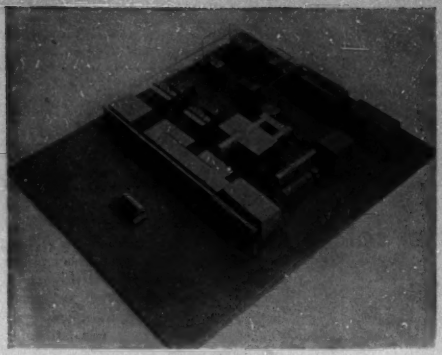


Figure 2. Model of single floor layout showing access space

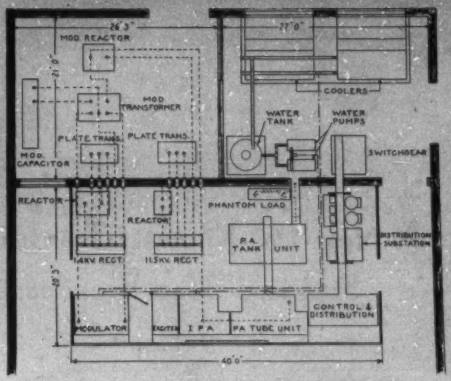


Figure 3. Floor plan, single floor layout

High-power rectifier tubes considered were the type *870-A* and the type *857-B*. The *870-A* was considered from the standpoint that one rectifier could be used for d-c voltage supply to both the power amplifier and the modulator. In this case d-c voltage is limited, by the modulated rating of the *9C21*, to 12.5 kv for radio frequency power amplifier service. A conservative operating figure is 11.5 kv. At this voltage six type *9C21* modulator tubes would be required to modulate four type *9C21* power amplifier tubes because of the grid current requirements in modulator service. A driver of many times the power capability of the actual final design choice would have been required for a modulator tube complement of four *9C21* tubes at 11.5 kv. The alternative of two rectifiers, one operating at 11.5 kv to supply four *9C21* power amplifier tubes and the other at 14 kv to supply four *9C21* modulator tubes, was chosen as the more economical system. Each rectifier operates with six *857-B* tubes in a 3-phase full wave circuit. The cost of 12 *857-B*'s is $2280.00 as compared with a cost of $7050.00 for six *870-A*'s plus two extra *9C21*'s required in the modulator. Savings represented ($4770.00) neglect the additional cost for six versus four modulator tube positions. Cost of spare tubes must also be considered. The two high-tension rectifier transformers are energized from a single control circuit breaker so that no additional expense is involved in the primary supply.

Savings were also realized on rectifier filter components. Ripple voltage on the power amplifier supply could be considerably higher than would be required for the modulator because of the filtering contributed by the modulation reactor. The separate criteria for modulator and power amplifier filtering resulted in lower total filter component kilovolt-amperes than would have been the case with a single rectifier. The saving appeared to be greater than the cost of the additional rectifier.

The advantages of air cooling versus water cooling were studied. The final decision was in favor of water cooling because of dissipation limitations on available air cooled tubes such as the *9C22*. Here again two additional tubes would have been required in the modulator for air cooled operation and the additional cost along with a cumbersome air duct system appear to outweigh the lower maintenance advantage of air cooling. A study of water cooling circuits using four *9C21*'s in the power amplifier and four in the modulator indicated that a simple system with moderate pressure and with water flow in series through each pair of tube jackets would suffice.

A 460-volt 3-phase power supply was selected as the most economical to meet the technical requirements. Particular attention was paid to the necessity for high-speed protection on rectifier arc backs and high power tube gas flashes. 460-volt switchgear provides the required protection speed at a cost substantially less than for switchgear operating on higher voltages. A further advantage of the low-voltage system results from the fact that a 380-volt power supply is standard in a number of countries where the transmitter might be installed. The transmitter design proceeded on the basis that operation at either 460 or 380 volts was possible by means of taps on the two main rectifier transformers and on two auxiliary transformers included as part of the transmitter.

Performance specifications were set up and are referred to later with actual performance data. Certain other features were included to improve reliability for broadcast operating groups. Provision is made for emergency operation at reduced power with half the number of power amplifier and modulator tubes. The two radio frequency amplifiers preceding the final stage were equipped with an extra tube socket. A spare tube can be in place with water and air applied so that it is ready for immediate connection of filament, grid and plate supply in case of failure of the active tube. A water-cooled dummy load is provided for convenience on initial setup, routine maintenance measurements, or subsequent line-up during transfer to another operating frequency. Water coolers and pumps are in duplicate although one pump and one cooler will be sufficient on normal operation at most times. Both coolers are required only when cooling air temperature exceeds 40 degrees centigrade. Final installation atmospheric conditions were considered. Design proceeded on the basis that full power operation could be realized at 8,000 feet above sea level. Figure 1 is a block diagram showing major components and tubes of the final design.

Detailed Description

Figure 2 is a model of the complete transmitter as it would appear if installed on a single floor. A fireproof wall between the transmitter and the oil-filled

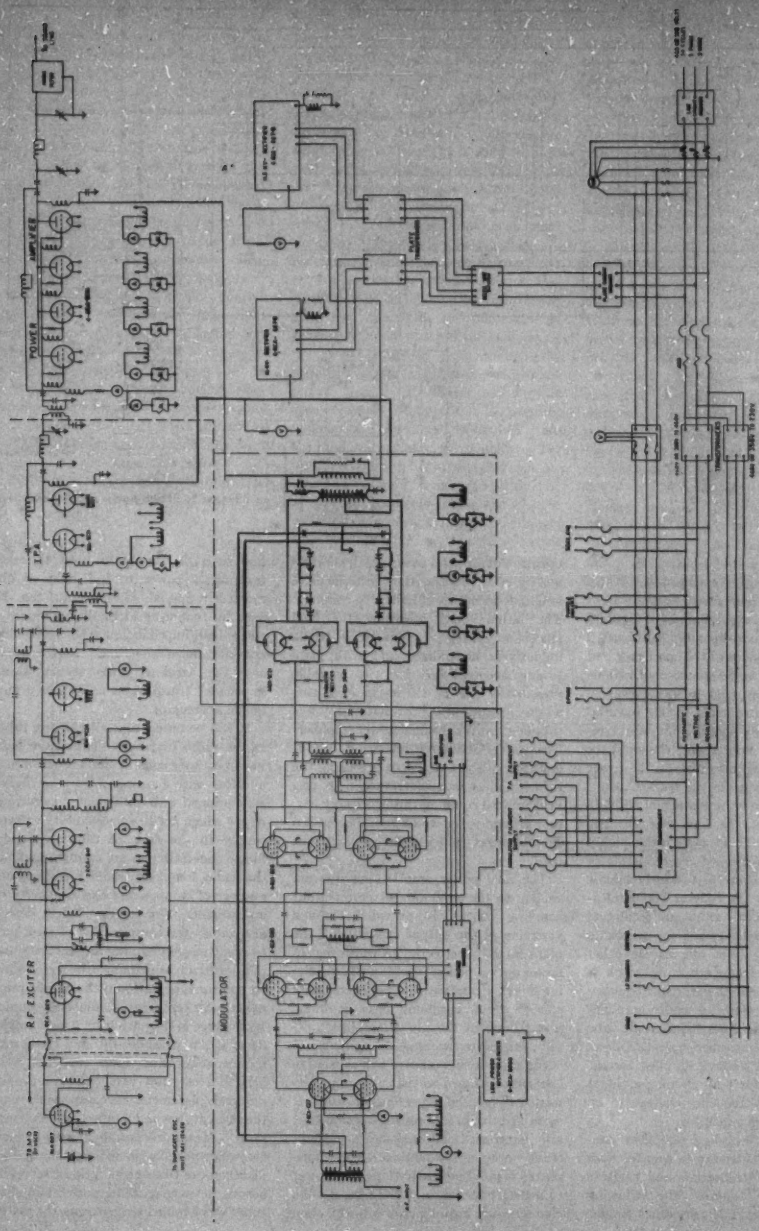


Figure 4. Fundamental circuits, 150-kw broadcast transmitter

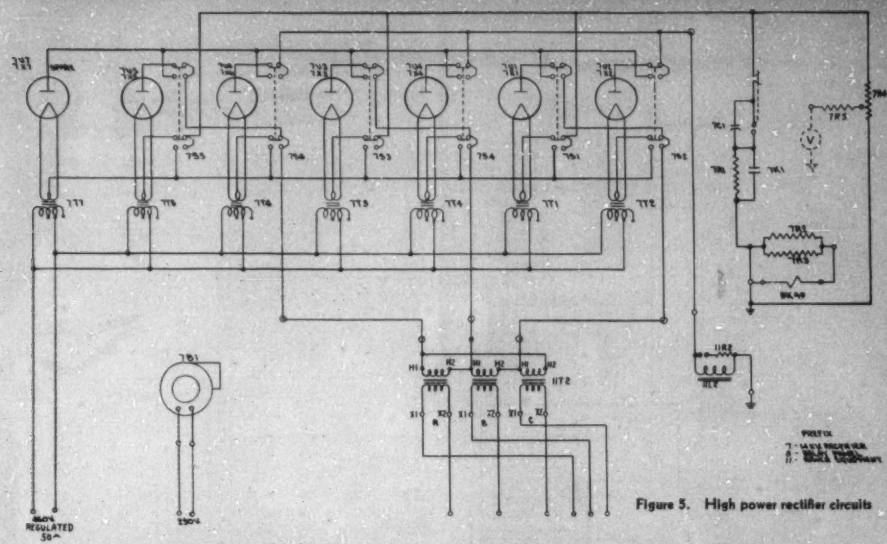


Figure 5. High power rectifier circuits

apparatus is omitted in this view. It will be noted that rear access space to individual units was well planned. Figure 3 is a floor plan corresponding to Figure 2 showing dimensions of the space required for installation and disposition of various units described in subsequent paragraphs. Design was co-ordinated so that installation with a two floor layout would require no more floor space, the auxiliary equipment being mounted on the lower floor directly beneath the transmitter.

Figure 4 shows fundamental circuits incorporated in the final design. The radio frequency chain starts with a highly stable, electron coupled crystal oscillator of a type approved for use in broadcast transmitters by the Federal Communications Commission. Duplicate oscillators are provided with means for switching instantaneously from one to the other without loss of program. A switch is available to select an external frequency source if so desired. A buffer amplifier consisting of one RCA *828* pentode tube with conventional tuned circuits follows the oscillator. Voltage dividing capacitors in the buffer tank circuit provide a source of radio-frequency voltage to an external frequency monitor.

The first intermediate amplifier consists of two *810* triodes in parallel with transformer neutralization and variable inductor plate tuning. The following stage using one *7C24* triode has similar circuits and provides an output of approximately 2 kw to the third intermediate amplifier by means of inductive coupling. The latter stage, preceding the final power amplifier, uses one *9C21* triode inductively neutralized and with plate tuning accomplished by two variable pressurized gas capacitors in a pi network. Output of approximately 15 kw is supplied to drive the final power amplifier. The use of inductive interstage output coupling in the two stages preceding the final amplifier was incorporated to prevent mutual coupling through heavy current ground paths which would occur if a simpler direct coupling method had been used.

The final power amplifier circuits are similar to the preceding stage. Output coupling control is provided through variation of the output capacitor of the plate circuit pi network. A wide range of power output control is obtained by varying this capacitor with little effect on plate efficiency. A harmonic filter is interposed between the power amplifier output and the transmission line input. It is equipped with inductive and capacitive elements arranged so that a T or pi network can be used depending on which is more effective in suppressing harmonics at any harmonic load impedance encountered on final installation. The transmitter is rated normally to deliver power to a load impedance of 230 plus j0, the characteristic impedance of a 6-wire open line such as frequently used to carry transmitter power to the base of the radiator. Special components can be used for mounting in the harmonic filter where load impedance is a 50- to 70-ohm coaxial transmission line. In the latter case the harmonic filter serves as an impedance transformer as well as a harmonic attenuator.

It will be noted that all vacuum tubes in the radio frequency chain have individual cathode current ammeters and that all tubes with a rating of 2 kw or higher are equipped with individual over current relays which act to remove plate voltage supply in the case of tube overloads. Single ended circuits are used throughout the radio frequency chain for minimum number of components and simplicity in adjustment. The single ended design also avoids the complication of push-pull to single ended coupling for the final output. Circuits which might be responsible for spurious oscillation or harmonic resonance were kept to an absolute minimum. Inductance in the path from power amplifier plates to cathodes through the blocking capacitor and tank capacitor was held to a very low value through proper physical placement of components and the use of low-inductance interconnections. Motor drive on the tuning capacitors made possible the most advantageous placement of these units. Spurious oscillations between parallel power amplifier tubes was avoided by grouping the plates

on a common assembly and using the familiar resistor-inductor suppressor on individual grid connections.

Filament heating supply to the power amplifier tubes is double quarter phased so that each filament is excited 45 degrees advanced in phase from its adjacent tube. Single phase filament excitation will result in hum components of twice the power frequency due to the magnetron effect. Quarter phasing will eliminate the double frequency component and leave only those components of four times the power frequency or higher. The corresponding hum reduction is approximately 13 decibels. Double quarter phasing leaves only components of eight times the power frequency or higher and effects a further reduction of approximately six decibels. A total of 19 decibels is not actually realized because of other factors but experience has indicated that this system will provide at least 15 decibels reduction of the hum produced by the magnetron effect.

Modulator circuit design was an extension of principles learned in the application of an economical system used since 1941 on 50-kw transmitters. Amplifiers are push-pull throughout the chain. Low power circuits consist of a 2-stage voltage amplifier and include networks which maintain phase relations suitable for the application of feedback. The voltage amplifier is followed by a zero voltage gain, cathode follower amplifier which drives the modulator grids directly from voltage developed across the cathode networks. It will be seen that peak plate current of the cathode follower stage will be approximately the same as peak grid current in the modulator tubes. The proper choice of modulation transformer ratio and modulator d-c plate voltage provides adequate output for modulating the power amplifier even though the cathode follower driver has relatively low power output. Modulator efficiency is approximately 55 per cent at 100 per cent modulation. Feedback is applied from the modulator plates to the grids of the input voltage amplifier through a resistor-capacitor network having zero phase shift at frequencies many times the highest modulating frequency. A stabilizing rectifier is provided between modulator grids and cathodes. This rectifier acts to prevent spurious oscillations which might otherwise develop when modulator grid current swings negative during a portion of the audio frequency cycle. Spurious oscillations of small magnitude may still appear due to shock excitation by steep program wave fronts. These oscillations do not affect modulator efficiency and the voltage at the spurious frequencies is attenuated to a negligible value by the "L" section network between the modulation transformer secondary and the power amplifier. The network also acts to maintain constant modulator output impedance up to 10,000 cycles per second above which the impedance rises rapidly.

Modulator tubes have individual cathode current ammeters and over current relays for indication and protection similar to those provided in the power amplifier. Each pair of modulator tubes has filaments excited in quarter phase relation to provide a hum reduction of a theoretical 13 decibels. Actual hum reduction in this case is approximately 10 decibels. The hum is further reduced by the feed-back circuit. A single-phase full-wave rectifier in the modulator provides fixed bias for the cathode follower and modulator tubes. Differential control on a bias bleeder network provides means for balancing the modulator static plate currents at a value of 150 milli-amperes per tube. A 3-phase full-wave rectifier with a half voltage tap supplies plate voltage to all low power tubes in both the radio frequency and audio frequency amplifiers.

Control relays and contactors for all

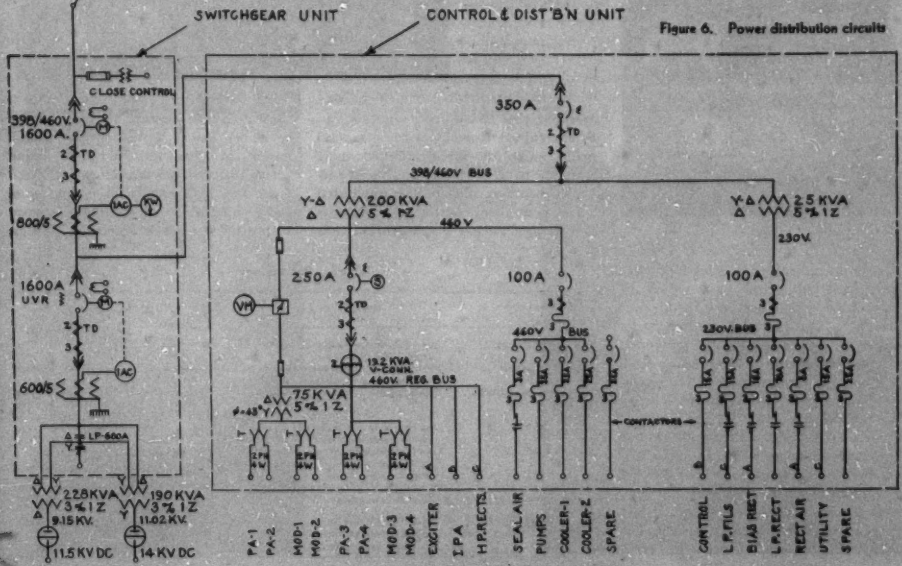
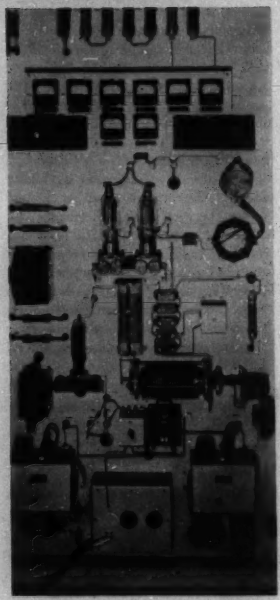


Figure 6. Power distribution circuits



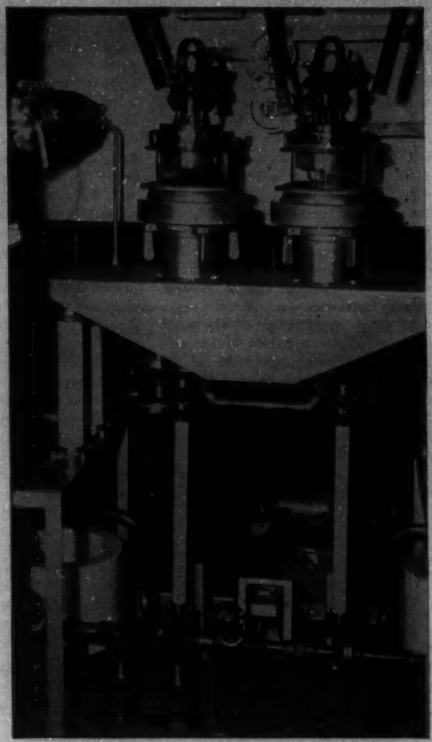


Figure 7 (left top). Front panel, 150-kw broadcast transmitter

Figure 8 (left). Crystal oscillators, buffer amplifier and first intermediate amplifier

Figure 9 (right). 15-kw intermediate amplifier showing spare tube in place

circuits are centralized in so far as possible in a single unit at the right end of the transmitter. This unit includes all over current relays and sequence relays required for proper starting and protection of circuits and tubes. A reclosing relay provides for three successive re-applications of power at spaced intervals when overload faults occur. If the overload persists after the third re-application of power, the relay will automatically lock out the closing circuit to prevent further re-applications of power unless manually reset by the transmitter operator. Control switches may be set up for complete automatic starting but switches are so arranged that the starting sequence can be interrupted manually at any of several points. Controls are distributed along the front panel of the transmitter at pertinent locations and are also duplicated on the centralized control unit. Circuits are arranged so that, if a control switch is open at any one location, power cannot be re-applied except by closing the control switch at that location. The usual safety features such as access door interlocks and high voltage grounding contactors are tied in with the control system to provide personnel protection. Water

Table I. Performance

	Specification	Actual
Frequency Range	Any specific frequency in the band from 540 to 1,600 kc	535 to 1,605 kc
Power Output	150 kw unmodulated carrier power at transmitter output terminals	155 kw within other specification limits
Frequency Stability	Plus or minus 10 cycles per second	Not measured. Identical frequency source previously measured well within specification limits
Carrier Shift	Less than five per cent at any modulation percentage up to 95 per cent	1.5 per cent positive at 100 per cent modulation
Noise Level	Less than 56 decibels rmn below 1,000 cycles per second. 100 per cent modulation	57 decibels
Radio Frequency Harmonics	70 decibels below fundamental measured at one mile	Not measured. Circuits are included to meet specification requirements
Program Input Level	10 milliwatts plus or minus two decibels for 100 per cent modulation at 1,000 cycles per second	10 milliwatts minus one decibel

Figure 10. 150-kw power amplifier showing assembly of four tubes on a single base plate

Figure 12. 11.5 kv and 14 kv rectifiers with associated filter reactors

flow interlocks are provided in the return line of each separate water circuit. These interlocks are of the indicating type so that a reduction in flow can be noted before flow drops low enough to open the interlock contact. Indicating water temperature interlocks are also provided on the discharge side of each separate water circuit.

A supervisory console duplicates essential transmitter controls and indicator lights. The console also has the necessary switching and metering for control of program level and transfer to local microphone or transcription turn-table when required. A monitor rectifier to be located at the radiator tuning house provides an audio monitoring signal and also serves to shut off carrier through an auxiliary control relay when lightning causes antenna flash overs. The rectifier also provides a remote antenna current indication on the front panel of the transmitter.

Figure 5 shows circuits of the rectifiers which supply the power amplifier and the modulator. A spare tube with filament energized can be switched in quickly in case of failure of an active tube. Figure 6 is a single-line diagram of high power switch gear and distribution circuits included in the transmitter. Auxiliary distribution circuit breakers are located in the control unit at the right of the transmitter. The incoming circuit breaker, the main rectifier circuit breaker and the delta-wye switch are located in a separate cubicle which may be placed near the main rectifier transformer to simplify heavy current interconnections. The delta-wye switch provides a convenient means for obtaining 0.57 times the normal anode voltage for initial adjustments and tests. Probable telephone line interference due to harmonics generated in the main rectifiers is reduced by connecting the high voltage side of the 11.5-kv rectifier transformer delta while the high voltage side of the 14-kv rectifier transformer is wye connected.

The incoming circuit breaker has time delay over current protection through an auxiliary induction type relay which can be adjusted so that the circuit breaker

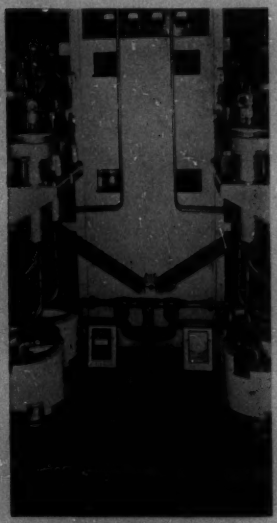


Figure 11 (left). Push-pul class B modulator
Low power tubes are visible through the rear panel

Figure 13. Water cooled dummy load, (left), enclosure for power amplifier tank coil, (upper), harmonic filter, (lower)

Figure 14 (left). Power amplifier plate tank coil

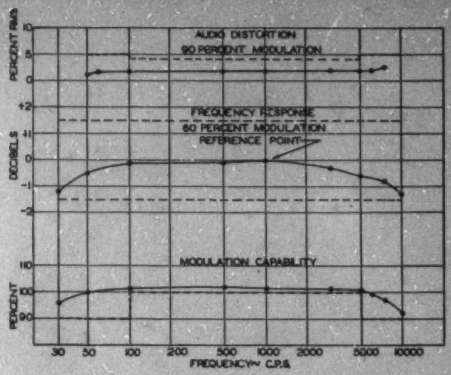


Figure 16. Performance

Dotted lines are specification limits

will not open on a fault unless all succeeding circuit breakers have failed to open. Time delay adjustment of this circuit breaker is effective up to ten times normal current rating above which the circuit breaker will open instantaneously. The circuit breaker feeding the high-power rectifier transformers is tripped by an auxiliary induction type over current relay which has adjustable settings for both inverse time and instantaneous operation. All other circuit breakers in the distribution system are provided with dash pot or thermo-element inverse time protection as well as instantaneous tripping at eight to ten times normal rating.

A one-to-one ratio, 200-kva transformer provides the impedance necessary to use circuit breakers of moderate interrupting rating in the secondary distribution circuit. This transformer is also equipped with taps so that the transmitter can be operated from a 380-volt power supply without changing taps or ratings on any transmitter elements in the secondary distribution system. Two single phase automatic voltage regulators connected open delta regulate the filament supply to high-power tubes to within 1.5 per cent. A 75-kva special transformer provides the 45 degree phase shift required by the double quarter phasing, filament excitation scheme described previously. Four *T* connected transformer pairs provide the second step for quarter phasing.

Water cooling is provided by a closed system including a distilled water storage tank, two pumps in parallel and two coolers in parallel. Valves are provided so that pumps and coolers may be cut in or out without interrupting transmitter operation. Coolers are the unit air conditioning type for water to air heat exchange. Additional valves in the power amplifier, last intermediate amplifier and modulator allow isolation of any two *9C21* tubes without shutting down the complete water system to change a tube.

Figures 7 to 16 inclusive show the final product as set up for test at the manufacturing plant. The general arrangement of various units on the test floor was carried out as near as possible in accordance with recommended final installation plans so that any peculiarities introduced by discrete connector lengths could be noted and corrected prior to final installation.

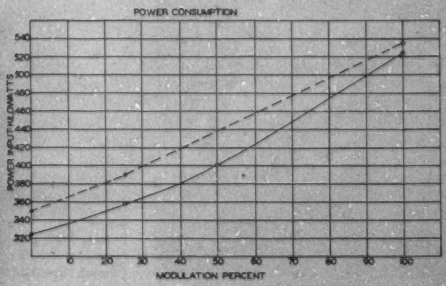
Performance

Figures 16, 17, and Table I show typical test floor performance data at 640 kc compared directly to performance specifications. It will be noted that an ample margin exists in all cases to cover variations which might be encountered in field installations. Final test, of course, showed up some faults which were overlooked during the design stage. These faults did not require any major changes but are noted here for their interest.

Figure 15. Modulation transformer and reactor (left); 11.5 kv and 14 kv rectifier transformers (right)

Figure 17. Power input

Dotted lines are specification limits



The modulator showed unstable operation at frequencies above 5,000 cycles per second when tests were started. Careful measurements and investigation showed that instability was a push-push oscillation which could be eliminated by obtaining closer coupling between the two halves of the push-pull circuit. Closer coupling was obtained by redesign for lower leakage reactance in the unity ratio transformer which supplies equal audio voltages on cathode, suppressor and screen of the modulator driver tubes. The first and second intermediate amplifiers of the radio frequency chain were inductively neutralized in the original design. Some difficulty was experienced particularly in the second intermediate amplifier because of critical adjustments. The transformer type neutralizing of the final design eliminated this trouble and provided broadband, noncritical neutralization.

Installation

Installation details were worked out carefully so that the ultimate user would find installation easy. Terminal locations are arranged so that interconnections between units can be made with standard overhead wire ducts. High voltage bus work required at the installation site is limited to a minimum of straight runs and bends in either the two floor or single floor layout. It is possible to install this transmitter within an existing building with a minimum amount of building modification. An installation material kit provides all bus work items, wire, insulators, water piping, valves, hardware and special tools required for installation with either the single floor or two floor layout.

Conclusions

A highly efficient, compact 150-kw transmitter has been developed for medium frequency broadcast service. Excellent performance can be obtained and maintained economically without employing highly skilled operating personnel. Component and tube ratings are such that the transmitter will provide adequate trouble free service for a long period at minimum cost.

The design principles embodied in the 150-kw transmitter can be extended to higher powers if and when required.

No Discussion

The "Superimposed Frequency Test" for Induction Motors, or S. F. Test

M. P. ROMEIRA
NONMEMBER AIEE

THE FEEDING of 3-phase currents to the slip rings of induction motors, either at slip frequency for speed regulation, or at any other frequency such as in differential motors for selsyn applications, is well known. The applications now in use suggest a method for testing induction motors under equivalent full-load conditions without the use of any kind of loading machine and with a minimum of power consumption.

This paper presents a method for taking a full-load slip ring motor efficiency and heat-run test under conditions of no-load power supply; a discussion of the theory of operation and the accuracy of the results; and a similar method for testing a squirrel-cage motor using a slip-ring motor as the load machine.

The procedure is simply to run the motor at no load with the slip rings short circuited through a voltage regulator or another alternator capable of providing full-frequency power supply. Sufficient voltage is applied to the rotor slip rings to circulate full-load current through the stator.

Connections and Operation

Figure 1 shows in single line diagram, the necessary arrangements and connections for this test. The operation is as follows.

A 3-phase voltage regulator (*VR*) is provided, capable of varying the supply voltage from zero to about one quarter of the open-circuit slip ring voltage. The regulator should preferably operate in conjunction with a transformer with windings arranged for various voltage and current combinations in order to extend the range of the set.

The primary (*S*) of the motor is connected straight to the supply; the slip rings (*R*) are connected to the output side of the voltage regulator (*VR*), preferably through a variable resistor (*WR*); and the input side of the voltage regulator is connected to the same supply of normal frequency voltage as the motor primary. Then

1. With the rotor circuit open at *WR*, close the supply to the stator, and measure the slip-ring volts.

2. Close the supply to *VR*, and adjust the voltage to one quarter or less of the open-circuit slip-ring voltage.

3. On gradually cutting out the resistance *WR* the motor will run up to speed, and if the direction of rotation of the stator field and injected rotor field are the same, the current on the stator will increase.

4. Adjust *VR* to give on the stator whatever current loading is finally required, and take readings of volts, amperes, and watts supplied to both the stator and rotor.

The voltage regulator *VR* may be replaced by an a-c generator, in which case the resistor *WR* may be dispensed with altogether. This, however, does not appear to be advisable.

It will be found generally that both stator and rotor currents are perfectly balanced unless there is some fault in the motor, and the wattmeters W_1 and W_2 connected in the primary and secondary circuits will read respectively the no-load losses, and the copper losses at the stator current loading used. The sum of the two gives the total losses, subject to certain corrections. The power factor in the primary circuit is constant and equal to that obtained under normal no-load conditions; the secondary power factor is substantially the same as that obtained under locked-rotor conditions.

Theory of the Superimposed Frequency Test

1. Consider the motor connected to the required test equipment as shown in Figure 1, and running light under full voltage and frequency with its slip rings short circuited, and the voltage regulator set unexcited; the wattmeter W_1 gives the total no-load losses. If the short circuit across the rings is removed, thus introducing the resistance of the *VR* secondary in the rotor circuit, the speed will drop very slightly and the reading of W_1 will increase slightly. Usually, the test transformer is considerably larger than the motor under test, so this increase will be negligible.

2. By exciting *VR*, a small voltage of normal frequency is injected into the rotor, which produces a field revolving in the same direction as the stator field (with the rotor). This field will cut the stator conductors at twice the normal speed (minus slip) generating in them a double frequency voltage *e* (Figure 2).

3. This voltage *e* combines with the stator applied voltage *E*, to give a resultant *ER* of normal frequency and somewhat distorted shape, leading the applied voltage *E* in one-half cycle, and lagging behind it in the next one-half cycle. This has a slight adverse effect in the losses which appear to be slightly increased by the rather peaked resultant voltage wave.

4. Relative to the rotor conductors, the speed of the revolving field created by the injected voltage *e* is the same as if the rotor

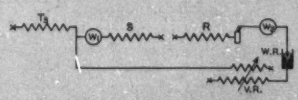


Figure 1. The primaries of both the motor (*S*) and the voltage regulating set (*VR*) are connected to the supply transformer (T_r)

The secondary or (*VR*) is connected to the slip-ring (*R*) of the motor preferably through a resistor (*WR*). Two sets of instruments W_1 and W_2 are connected respectively to the primary and secondary circuits

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M. P. Romeira is designing engineer, General Electric Sociedade Anonima, Rio de Janeiro, Brazil.

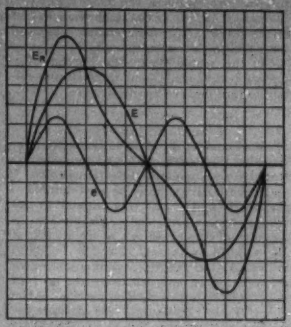
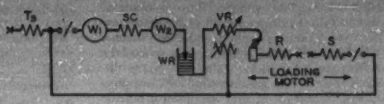


Figure 2. The normal frequency voltage *E* supplied to the stator, combines with the double frequency voltage *e* induced thereon by the rotor double speed flux, giving a resultant voltage *Er*, whose peaked wave form may affect somewhat the iron loss

were stationary. Hence the rotor impedance to this voltage is the same as would be found in the "locked rotor test." For a given injected rotor voltage, and, therefore rotor field strength, the voltage generated in the stator conductors is twice that for stationary conditions and so is the impedance due to the double frequency. Therefore the current induced in the stator will have the same value as for locked rotor conditions.

The only difference is that the stator is not short circuited upon itself, as it would be for the normal short-circuit test taken from the rotor side, but is, relative to these double frequency currents, short circuited through the secondary of the supply transformer.

Figure 3. A suggested arrangement whereby a squirrel cage motor (*SC*) may be tested, using a slip ring motor (*SR*) as a loading machine, the two machines running independently



5. An extra copper loss will thus be introduced which can be calculated once and for all for a given supply circuit.

Thus, the iron and other no-load losses are supplied at normal frequency and are therefore practically unaffected except for the slightly higher saturation due to the somewhat peaked wave forms. The copper loss is unaffected by the frequency, except in respect to eddy currents, for which correction curves will have to be worked out.

An idea of the usefulness of the test may be had from the following readings taken on a 40-horsepower 380-volt 50-cycle 725-rpm motor, supplied by a 150-kva transformer.

Stator volts = 380 amperes = 52.3
Rotor volts = 150 amperes = 93.0

The following readings were taken:

(A). No load test: $V_1 = 380$; $A_1 = 12.5$; $W_1 = 1.125$ kw; $\cos\phi_0 = 0.136$
(B). Locked rotor: $V_1 = 78$; $A_1 = 50.0$; $W_1 = 3.2$ kw; $\cos\phi_k = 0.476$
(C). *SF* test: $V_2 = 30$; $A_2 = 123$; $W_2 = 3.02$ kw; $\cos\phi_s = 0.472$

Subscripts 1 and 2 refer to readings taken on the primary or secondary respectively and *SF* is the superimposed frequency test.

The locked-rotor test was taken for comparison purposes only and the superimposed frequency test was taken with 51 amperes on the stator.

The short circuit current under full voltage is from test *B*

$I_s = 50 \times 380/78 = 243.5$

from *C*

$$D_s = \left(123 \times \frac{150}{30}\right) \times \frac{150}{380} = 243$$

(referred to stator)

Only a slight change occurs in the stator readings (W_1) when the rotor excitation is taken off; that is the no-load losses and power factor remain constant, therefore whatever energy is supplied to the rotor in the *SF* test must be entirely copper losses (I^2R and eddy loss) and equal the total copper loss of the motor.

Squirrel Cage Motors

It is believed that a modification of this arrangement, as shown in Figure 3, may enable us to provide a simple loading device for squirrel-cage motors.

The author realizes that the complete investigation of the various phenomena involved has been left untouched and hopes that tests now being carried on in Schenectady will clear up the many doubtful points.

Discussion

W. M. Schweder (General Electric Company, Schenectady, N. Y.): The superimposed frequency test proposed by Romeira may well prove to be of practical usefulness but a good deal of study will be required to work out a reliable procedure. It provides a means for measuring the stray losses of an induction machine in a single test, using only electrical measurements, and a means of simulating full load current and heating conditions without actually loading the machine. Also, it may be useful for design investigations.

To throw light on the accuracy of this method, tests have been made recently on a standard 20-horsepower 4-pole 60-cycle 440-volt 3-phase slip-ring induction motor. The motor was direct coupled to an electrically independent driving motor, and was tested under four separate conditions.

In the first test, the rotor was short circuited, and normal 3-phase excitation was supplied to the stator, while the driving motor was idling.

In the second test, the motor was driven at rated speed by the driving motor, the stator was short circuited, and 60-cycle reversed field excitation was applied to the rotor slip-rings, sufficient to produce full load rotor current.

In the third test, the conditions were the same as for test 2, except that the stator circuit was closed through the power supply system with no excitation on the primary side.

In the fourth test, the driving motor was again idle, the motor was driven from the primary side with rated voltage and frequency applied, and rated frequency reversed rotation voltage was applied to the rotor to produce full load current.

The test data are given in Table I for the four tests, the volts in each case being line to line, and the amperes the line values, under balanced polyphase conditions.

To analyze these data, it is convenient to consider the system as formed of two entirely independent circuits superimposed on

Table I

Test Number	Stator Volts	Stator Amperes	Stator Watts	Rotor Volts	Rotor Amperes	Rotor Watts	Mechanical Power Delivered by Driving Motor Watts
1	440	12.5	3,614	0	0	0	0
2	0	0	0	28	59	955	538
3		23.8	364	32.5	59	1,117	620
4	440	26	3,636	26	59.25	1,038	0

each other. The primary 60-cycle source of power delivers power to the stator, and produces a secondary slip frequency current, which creates some losses in the secondary power supply system. The secondary 60-cycle source of power similarly supplies power to the rotor, and produces a double frequency stator current, which circulates in the primary power supply system. The wattmeter in the stator circuit, therefore, reads the power supplied by the primary source, less the loss created in the primary system by the induced double frequency current.

Similarly, the wattmeter in the secondary reads the power supplied to the motor from the secondary source, less the losses created in the secondary system by the induced slip frequency current.

Let

I_1 = 60 cycle primary current

I'_1 = corresponding slip frequency secondary current

I_2 = 60 cycle secondary current

I'_2 = corresponding induced double frequency primary current

r_1 = primary resistance = 0.265 ohm per phase

r_2 = secondary resistance = 0.051 ohm per phase

r_3 = effective resistance of primary external circuit

r_4 = effective resistance of secondary external circuit

In tests 1 and 4, the power input to the primary supplies all of the rotational losses. In tests 2 and 3, the rotational losses are supplied by the driving motor, and they do not appear at all in the electrical measurements.

On this basis, the measured watts input to the stator, W_1, is made up of eight components:

A. The running light watts, made up of friction and windage, core loss, and magnetizing current copper loss. By test 1, this total is 3,614 watts.

B. The secondary copper loss due to the small slip frequency rotor current, $3I_1'^2r_2$.

C. The additional slip frequency loss produced in the external secondary circuit, $3I_1'^2r_4$.

D. One half the copper losses due to the double frequency primary current,

$\frac{3}{2} I_2'^2r_1$.

This is the part of this loss that is supplied by rotation.

E. The stray load loss caused by pulsation of the air gap leakage fluxes (due to rotation).

F. One half the double frequency stator core loss and leakage flux losses due to the double frequency current (the part of this loss supplied by rotation).

$G = \frac{H}{2}$. One half the double frequency losses produced in the primary power supply system by the double frequency current (the part of this loss supplied by rotation).

H. From the total of the foregoing, there must be subtracted the double frequency loss delivered to the primary system, $-3I_2'^2r_1$.

The measured watts input to the rotor is made up of six components:

K. The rotor copper loss due to the 60-cycle current, $3I_2r_2$.

L. The 60-cycle rotor core loss and losses due to leakage flux crossing the conductors (assumed negligible in this case, since the rotor has a wire winding). This loss may be large in a motor with a bar winding in the rotor.

$M = D$. One half the stator copper loss due to the induced double frequency current, $\frac{3}{2}I_2'^2r_1$ (the part of this loss supplied by transformer action).

$N = F$. One half the double frequency stator core loss and leakage flux losses due to the double frequency current (the part of this loss supplied by transformer action).

$P = G$. One half the double frequency losses produced in the primary power supply system by the double frequency current (the part of this loss supplied by transformer action).

$Q = C$. From the total of the above, there must be subtracted the slip frequency losses delivered to the secondary power supply system, $-3I_1'^2r_4$.

In test 2, W_1, F, and Q are zero, $K = 533$, $L = O$, $M = 229$. Therefore, $W_2 = 955 = K + L + M + N = 762 + N$, where $N = 955 - 762 = 193$ watts.

The mechanical input supplied by the driving motor in this test, in excess of friction and windage, was found by test to be 538 watts, and is equal to $D + F + E$.

Therefore, $538 = 229 + 193 + E$, and stray load loss $= E = 538 - 422 = 116$ watts.

In test 3, $W = -364$, $A = O$ (since the driving motor supplies the 1 friction and windage), $B = O$, $C = O$, $D = M = 229$, $E = 116$ (by test 2), $F = N = 193$ (by test 2), $K = 533$, $L = O$, $Q = O$. D and E are supplied by the driving motor.

Therefore, $W_1 = -364 = -H$, and $H = 364$ watts, whence $G = 182$ watts.
Also,

$$W_3 = 1{,}117 = K + L + M + N + P + Q$$
$$= 533 + O + 229 + N + 182 + O,$$

whence $N = 1{,}117 - 944 = 173$ watts, instead of 193 as found by test 2.
And,

$$620 = D + E + F + G$$
$$= 229 + E + 173 + 182$$

whence $E = 36$ watts, instead of 116, as found by test 2.

In test 4, $W_3 = 1{,}038 = K + L + M + N + P - Q$
$= 533 + O + 229 + N + 182 - Q$.

Taking $N = 183$, the average of tests 1 and 2, $C = Q = 1{,}127 - 1{,}038 = 89$ watts.
Also

$$W_1 = 3{,}636 = A + B + C + D + E + F + G - H$$
$$= 3{,}614 + B + 89 + 229 + E + F + 182 - 364$$

B is assumed to be negligible,
whence $E + F = 3{,}636 - 3{,}750 = -114$ watts,
instead of $116 + 193 = 309$ watts by test 2, or
$36 + 173 = 209$ watts by test 3.

This lack of agreement is quite unsatisfactory, and has not been explained as yet.

It should be pointed out, however, that there is a large 60-cycle pulsating torque under test 4 conditions, due to the coexistence of the full load double frequency current and the full voltage normal frequency flux in the stator (or the 60-cycle rotor current and the slip frequency rotor flux). This pulsating torque creates oscillations in the rotor speed and phase position, that are larger the smaller the inertia of the rotor. A check test made with a smaller flywheel effect in the rotor showed about 10 per cent higher losses. This would be expected, since the oscillations in the slip cause corresponding oscillations in the rotor resistance as viewed by the stator, giving rise to pulsations in the load current drawn from the primary power supply. These current pulsations create extra copper losses in both stator and rotor. The effect is the same as occurs in loom motors, whose load pulsates due to the shock of the flying shuttle.

The test can be made with less than normal frequency applied to the rotor, in which case the extra losses not occurring in a normal motor (F, G, L, and so forth) will be reduced. However, the amplitude of the speed oscillations will be increased.

A check was made on the pulsating torque in test 4, by mounting the motor on rubber blocks and measuring the angle of oscillation of the stator. The observed value checked fairly well with that calculated.

On the whole, this superimposed frequency test presents some interesting possibilities that deserve further study.

M. P. Romeira: In reply to Schweder's interesting discussion, and analyzing the exhaustive tests made, I note that in tests 1 and 4 the auxiliary motor he used coupled to the motor under test was allowed to run idle, and not uncoupled; this resulted in readings of primary input to the motor, of W_1 = 3,614 watts (test 1) and W_1 = 3,636 in test 4 (the SF test) neither of which represents even approximately the no load losses, which for this class of motor would be about W_1 = 1,000 watts; the watts representing the running losses of the idling rotor should be shown in the last column of Table I with a minus sign.

These two tests show, however, that the primary input remains practically constant for any phantom load thrown on the stator; and therefore adding W_2 = 1,038 watts supplied to the rotor in test 4 we have the total losses under SF conditions.

Hence the true efficiency = $20 \times 0.746/(20 \times 0.746) + 2{,}038 = 14{,}920/16{,}958 = 0.88$ is immediately obtained, subject to correction for the loss incurred in the resistance of the primary and secondary external circuits (r_3 and r_4) which was not given; this loss can always be readily obtained from sets of curves worked out for any given test floor setup.

Referring to tests 2 and 3, I take it they were both carried out with the rotor field revolving in the same direction as the rotor (as in test 4); unfortunately it would seem that typing mistakes or the like have crept into Table I putting zeros in the stator amperes and watts columns for test 2, instead of the value actually found $I^{2'}$ = 24 (as worked back from Schweder's figure in test 2 for $M = \frac{3}{2}(I_2')^2 . r_1 = 229$) causing considerable headache in trying to find out how zero amperes could be obtained in the shorted stator, with 59 amperes on the rotor; as this could be possible only with the rotor field going in the opposite direction to the rotation of the rotor, and the latter at synchronous and not rated speed, a doubt naturally occurred as to whether tests 2 and 3 were actually made in the same conditions; altogether I believe there are too many zeros in Table I, simply meaning that the respective readings were not taken, as in test 1 under the heading Rotor.

Tests 2 and 3 are specially designed to investigate if and to what extent the principle of operation of the proposed method of testing may affect the various losses and particularly the stray load loss, whose determination as separate from the "known" losses (core loss, friction and windage and primary and secondary I^2R loss) is of interest to the designer only.

In the SF test which is intended for the test floor, we get the total losses given us directly by the addition of two readings ($W_1 + W_2$), the stray loss being therefore included. In the usual tests, we get the first two items from the light run watts, and then with the motor stopped we measure separately the resistances r_1 and r_2 of the stator and rotor windings and calculate the

respective I^2R loss; the stray load loss is therefore not included, and must be determined by other tests, if we want to get the true efficiency. This is another advantage of the SF test.

Roughly I believe that, though the stray loss is partly iron and partly copper loss, since it is all due to rotation, it is supplied entirely by the primary supply in the SF test; and therefore the small difference in W_1 between tests 1 and 4 ($=22$ watts) represents the stray load loss multiplied by

$$\frac{\text{slip in test 4}}{\text{designed } FL \text{ slip}}$$

Thus, if the designed full load slip $= 5$ per cent, and we find 1 per cent slip under test 4, then the stray load loss $= 22 \times \frac{5}{1} = 110$ watts, if the tooth saturation is not too high; this appears to check fairly well with the value found by Schweder from test 2 which I believe is correct, since W_1 not being $= 0$ does not affect the result. But, in my opinion, it does affect indirectly test 3 because on passing from test 2 to 3, the stator watts decrease by $458 - 364 = 94$; and since the stator current remains practically the same $I'_2 = 23.8$ (instead of 24) for the same rotor current, the flux leakage will remain the same; but the stator power factor and watts have decreased. Therefore, to keep the speed constant, the input to the shaft has to decrease an equal amount; and 94 watts less than in test 2 must be subtracted from the total watts supplied to the shaft in test 3 to get the corresponding figure in the last column, or else added to that figure.

It seems therefore that instead of 620 in the last column for test 3, we should have $620 + 94 = 714$ watts.

Then $714 = D + E + F + G$ and $E = 714 - 584 = 130$ watts instead of 36, and $E + F = 130 + 173 = 303$ watts, which agrees quite closely with $E = 116$ and $E + F = 116 + 193 = 309$ from test 2.

Regarding test 4, it is not stated whether the supply to the rotor was drawn from the same source as the supply to the stator through a voltage regulator (as indicated in my paper) or from an independent generator; either way is permissible when the heat run only is desired; but when accurate segregation of the losses is required, I am afraid that any differences in wave form and absence of synchronism between the two independent supplies may introduce all sorts of unnecessary troubles, which may account for the discrepancies found.

Another advantage of this test is that, as is well known the impedance voltage taken in the usual locked rotor test, varies considerably with the position of the rotor, and an average value must be taken from a number of separate readings; while in the SF test with the rotor going at full speed the impedance values are automatically averaged out. Regarding the 60-cycle torque pulsations referred to under test 4 (SF test) conditions; it is not stated whether these pulsations are in the same or opposite direction for each successive pulsation or, if always in the same direction, (as might be inferred from the text since they appear to affect the speed) whether with or against rotation.

I was convinced from many tests made on different makes of motors, that these pulsations (barely noticeable as a characteristic rumble) do not affect the speed; I never noticed it on a tachometer and was under the impression that they cancel out not affecting either speed or phase position, though I admit not to have paid particular attention to that point. In fact, I found these pulsations a useful by-product when in a certain case they were the means of detecting audibly, a very bad case of a stator core loosely held by pressure screws to the outer ring frame; in this case, the horrible vibration was immediately checked by tightening up, which, without the SF test might have passed unnoticed. This would appear to indicate that the successive torque pulsations are in opposite directions; since were they in the same direction they would not cause such vibration, the stator pushing always in the same sense, instead of a push-pull action; and again, these pulsations might become less important if as explained before and I always did in my tests, the stator and rotor are supplied from the same and not independent sources.

All I can say in this connection is that if there are any pulsations in the load current drawn from the primary, they were never in my experience of a magnitude to be even noticeable.

In conclusion, it should be borne in mind that the test was proposed primarily as an economic method of giving a motor the required heat run at much smaller expense in power consumption as well as time and labor; the idea of using it simultaneously for the segregation of losses was only a more or less obvious corollary; and these can always be obtained by other methods if preferred, since it is not the determination of the losses that costs money; it is the heat run and preparations for the same.

Utilization of Electric Power in Open-Pit Copper Mining

S. F. FRENCH
MEMBER AIEE

H. SPEIGHT
MEMBER AIEE

Synopsis: At the outset it should be said that the utilization of electric power in the mining of copper ore as in many other branches of engineering requires only the application of sound electrical engineering knowledge, with particular attention applied to some of the special problems involved. It is well known to members of the Institute, and of the electrical engineering profession in general, that each industrial operation involves special features differing somewhat with the various processes, and so it is with the subject of copper mining. It becomes necessary, in order to illustrate specific principles, to restrict ourselves to the mining of one type of ore. It is our belief that many of these special problems arise in processing sulphide ore and the following outlines this interesting process together with descriptions of equipment and discussion of some of the problems involved.

Mining

THE TERM "open pit" is applied mostly to comparatively low-grade ore deposits which can be worked by open cuts or benches and where no underground mining is entailed. These pits are sometimes located on the side of mountains and other times are located in fairly level country, so that the pit proper may actually be a series of benches along a mountain side, or an open excavation ranging from several hundred to a thousand feet deep. In either case, the removal of the copper-bearing ore involves the moving of large tonnages of ore and waste daily.

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S. F. French is electrical engineer, Anaconda Mining Company, New York, N. Y., and H. Speight is district engineer, Westinghouse Electric Corporation, New York, N. Y.

For this purpose all other equipment has been largely superseded by the electric shovel, which is usually of 4 to $4^1/_2$ cubic yard capacity. These shovels are capable of loading up to 5,000 tons of ore per 8-hour shift. Modern equipment is of the full revolving type, the majority of which are operated by d-c motors on all motions. Power for the motors is furnished by a variable-voltage motor generator set which greatly facilitates control and ease of operation. A $4^1/_2$ cubic yard full revolving shovel is illustrated in Figure 1 and a typical schematic diagram of main driving motor, generators, and individual drives is shown in Figure 2. The a-c power supply is conveyed to the shovel through flexible 3-conductor cables, which are referred to later. Amongst the other electrically-operated equipment used in open-pit mining is the churn drill, which is used for drilling holes in preparation for blasting. Power supply for the shovels, churn drills, and other equipment in the mine, is usually brought to the pit edge at voltages from 6,600 to 13,800 volts over the primary lines, and is then transformed at one or more points to 2,300 or 4,100 volts for shovels, and 440 volts for churn drills. From the secondary line, which generally encircles a large portion of the pit, distribution is made through portable switch houses for the shovels. These switch houses contain a suitable circuit breaker and relaying apparatus for shovel and cable protection. A similar distribution center known as a churn-drill house is used to provide power for the churn drills. These are fed also from the 2,300-volt secondary line and contain, in addition to disconnect devices, a transformer of sufficient capacity to operate two or three churn drills. The former are usually transported from one location to another by means of a boom and the latter, which are usually mounted on skids are moved about by caterpillar tractors as required. A typical shovel-switch house diagram is shown in Figure 3, which also illustrates the diagram for a churn-drill transformer house. Shown also is a grounding scheme which is discussed in detail later.

Because of the importance of maintaining power service to the shovels, a brief description of the cables used, and methods of protecting same, as well as the operating personnel, is included.

Figure 1. A full-revolving $4^1/_2$ cubic yard electric-shovel loading-ore train

The capacity of each car is 70 tons. Churn drills appear on bench above the shovel

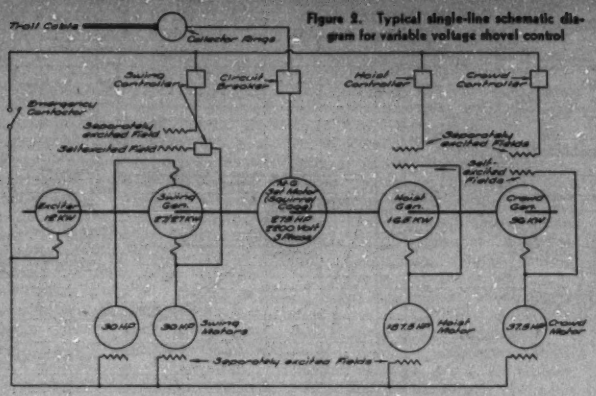


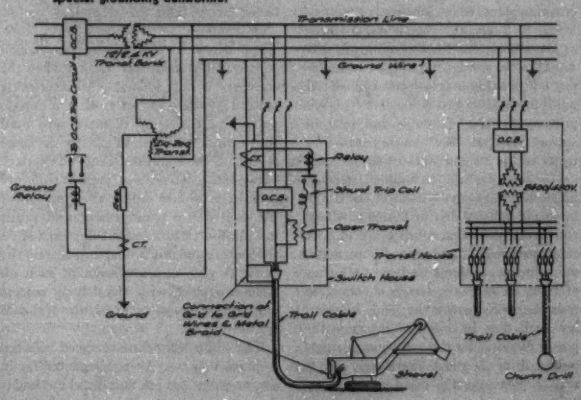
Figure 2. Typical single-line schematic diagram for variable voltage shovel control

These cables are usually 3-conductor, insulated with a high grade of rubber insulation and jacketed with a tough outer covering of Neoprene or other abrasion resisting compound. A metallic sheath is frequently employed over each conductor, which serves the purpose of distributing the electrostatic stresses and can also be used for ground connections. In addition, three ground wires are sometimes incorporated in the interstices, thus providing ground-return conductors of considerable capacity. In most open-pit mines, due to the nature of the terrain, it is difficult to provide proper grounding at the shovels. If this is not done, severe electrical shocks may be experienced by the personnel in case of a ground on the shovel apparatus or wiring, when entering or leaving the shovel or touching any part of the framework while standing on the ground. In the case of grounded neutral power-supply systems, protection is obtained by connecting the ground wires to the shovel frame and to the neutral of the transformer bank and/or low resistance ground sources near the switch houses or power lines. In the case of delta-connected power transformers, which are normally ungrounded, excellent protection can be obtained by application of a 3-phase so-called zigzag grounding transformer, which provides a low-impedance ground path for return currents. If the neutral thus obtained is carried on the distribution lines, through the switch houses and ground wires in the cables to the shovel, this method possesses another advantage in that selective relaying of the parallel feeders can be readily obtained in addition to limiting the rise of voltage on the shovel frame in the event of a ground fault in the shovel equipment. A schematic diagram of equipment designed for this purpose is shown in Figure 3. It will be noted that a resistor is connected between the grounding transformer neutral and ground connections. The purpose of this is to limit the amount of ground current which can flow into any fault and is a desirable feature of this system. For normal application the maximum current which is allowed to flow in a ground fault is limited to values between 12.5 and 25 amperes.

Transportation

At most properties, the usual practice is to locate the reduction works or plant for milling the ore as close to the pit as possible to reduce the cost of transporting large tonnages of ore, of which the greater part is discarded after separation. The type of transportation used between the pit and the mill depends on several factors, such as the topography of the ground, length of haul, and tonnage to be handled. Where large tonnages are handled, if it is practical to do so, rail haulage is usually employed, and in many modern systems these are electrified. Other forms of transportation are truck haulage, hoisting, or a combination of any of these. Types of locomotives used on pit haulage systems include steam, diesel-electric, trolley or third-rail straight electric, and trolley or third-rail combination locomotives with auxiliary power, for operation on benches supplied by storage batteries or diesel engines. Because of lack of sufficient space, description of these systems must be omitted.

Figure 3. Diagram showing main circuits of shovel and churn-drill switch houses and special grounding transformer

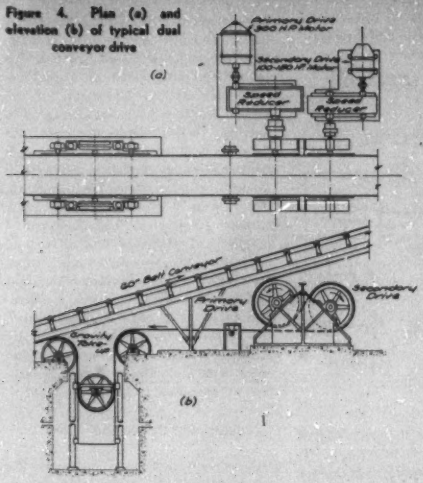


Crushing and Conveying

After transportation from the mine the ore is delivered to the crushing plant, where it is reduced to about ten inches in size by passing through the primary crusher. These may be of the jaw type or the gyratory type, but in either case, call for the application of electric motors usually ranging from 300 to 500 horsepower capacity and especially designed for rugged and continuous service.

In the larger sizes, wound-rotor motors with pedestal-type bearings are frequently used because of the importance of maintaining uninterrupted ore flow. In such cases the starting equipment for 2,300-volt motors often consists of a heavy duty circuit breaker with automatic acceleration. This, together with other electric equipment, is best located in a dustproof

Figure 4. Plan (a) and elevation (b) of typical dual conveyor drive



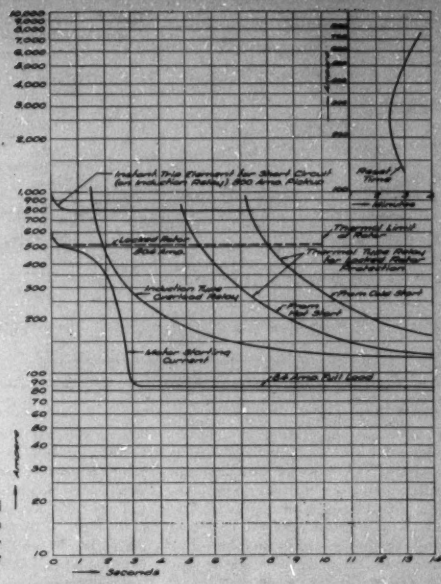


Figure 5. Typical relay protection curves for 300-horsepower crusher motor

control room or substation within or near the primary crusher building and provided with remote controls extending to the control desk on the operating floor. At the control desk are also located the "Start" and "Stop" buttons for the primary-crusher lubricating system and other necessary auxiliaries together with emergency "Stop" buttons and indicating lights for the feeders and conveyors on which the output of the crusher is carried.

From the gyratory crusher the ore is discharged onto conveyors, which deposit it in storage bins of sufficient capacity to adapt the intermittent operation of the primary crushing plant to the continuous operation of milling. From the storage bins, the ore is conveyed to the secondary crushing plant, where it is further reduced in size by a series of cone or roll crushers, or a combination of both, and again conveyed to the storage bin at the concentrator. From this it is seen that all of the ore is handled by belt conveyors from the primary crushing plant up to and into the storage bin at the concentrator. In many of the larger plants these belt conveyors must be built to handle large tonnages which frequently involve the use of large and unusual driving motors. These conveyors almost invariably are of considerable length and designed for a maximum incline, without backward slippage of rock, to keep the length as short as possible. Frequently the loads imposed are such that in order to transmit the power required for the conveyor, a single pulley drive becomes excessively large, and to overcome the disadvantages and cost of enormous head pulleys, the dual conveyor drive has been developed. This consists of a pair of driving motors, referred to as the primary and secondary drives, respectively, which drive two independent sheaves, located to obtain a maximum pulley wrap by the belt. By proper design the total stress in the belt is thus reduced and consequently the life is extended and cost reduced. For this type of drive, the motors are of different sizes, with the primary motor two to three times larger than the secondary motor. This combination results in a lower slack-side tension than is otherwise possible and consequently less total tension when the conveyor is fully loaded. An interesting feature of these drives is that although pulleys of the same diameter are used they cannot be driven at exactly the same speeds, as the slight difference in slippage between the two pulleys, present with any belt drive, would defeat the purpose of the dual motor arrangement and result in disproportionate loads being carried on the motors and much wear and tear on the belt. A plan and elevation of such an arrangement showing the driving motors and pulleys is shown in Figure 4(*a*) and (*b*).

The slight difference in speed referred to previously may be compensated for in several ways, but one successful method is to use slip-ring motors having the same speed-torque characteristics and including a permanent slip resistor in the secondary side of the secondary driving motor. If this resistor is equipped with taps an initial adjustment can be made to insure correct division of power between the two motors, which will then divide the load in proper proportion over range of loadings imposed on the conveyor. Smooth and positive acceleration on both motors is insured by providing several accelerating points, controlled automatically from a master panel which keeps both motors in step throughout the acceleration period.

Amongst other noteworthy features is the system employed to prevent any series conveyor from overloading the preceding conveyor or the equipment which it is feeding, in case of accidental stoppage of the latter. This is done usually by interlocking all conveyors in series with each other and with equipment they may be feeding, by a combination of electrical and mechanical interlocks so arranged that if a crusher stops for any reason the conveyor or conveyors feeding it are in turn stopped to prevent burying the equipment in ore.

In general, the primary crushers are usually driven by slip-ring induction motors, while the secondary crushers may be driven by slip-ring, squirrel-cage, or

synchronous motors. On heavy drives of this type which are subjected to very severe service and abuse it is necessary to incorporate adequate relay protection so that burning out of the crusher drive motors will not result from repeated attempts to start a choked crusher. This protection is usually obtained by a combination of relays designed to protect the motor against prolonged high-inrush currents, prolonged overload or repeated closing of circuit breakers to a stalled motor. A typical relay protection curve for these drives is shown in Figure 5. Inspection of these graphs shows that each element or relay protects the motors under different conditions which can frequently occur. At first glance it may appear that the thermal relay protection is not needed because the induction-type overload relay will remove the motor from the line in case locked rotor current persists. In case of a choked crusher, however, repeated attempts may be made to start, thus burning out the rotor in a short time unless this contingency is guarded against. It will be noted that in the specific case shown in Figure 5 that locked rotor current will cause the induction-type relay to trip in two seconds. However, this relay resets instantly and the starting could be repeated indefinitely. On the third attempt, however, the elapsed time has reached the zone covered by the thermal relay, which therefore trips the motor breaker and prevents restarting in accordance with the resetting curve shown also in Figure 5. This enforced delay gives the motor rotor time to cool before attempts to start it can again be made.

Concentrating

From the concentrator ore bins the crushed sulphide ore is fed to the grinding mills, in which wet grinding is employed. They are continuously charged by automatically-controlled feeders and conveyors which deliver ore into the mill at a predetermined rate of feed. The output of the grinding mills, of which the most commonly used type is known as the ball mill, then passes successively through the classifiers, flotation section, and filter plant. The oversize from the classifiers is returned to the input of the ball mill as feed, while the overflow containing the values and the fines are carried in launders to the first of a series of flotation machines. By the addition of certain reagents at this stage, and by suitable agitation, the values are recovered in the form of a froth, while the sands are depressed and carried away to the tailings disposal area. From the final flotation sections, the concentrated ore, now containing approximately 30 per cent copper, is carried by launders to thickening tanks, where the water is reclaimed and the thickened concentrate transferred to the filter plant. In the filter plant the concentrate is passed through mechanical filters which remove most of the water, leaving the concentrated and finely-ground products as filter cake.

It is in the concentrator proper that the bulk of electric power is used, and which calls for a great deal of ingenuity on the part of the designers in distributing comparatively large amounts of power in so crowded an area. An idea of the problems involved may be gained when it is realized that in a concentrator with a capacity of 25,000 tons of ore per day, some 20,000 kw is utilized. The largest single users of power are the ball or grinding mills, the largest sizes of which are driven by 800-horsepower synchronous motors, each of which drives a mill handling from 1,500 to 2,000 tons of ore per 24-hour day. As each of these motors constitutes a considerable load, it is advantageous, providing the main substation can be located near the center of the load, to supply each motor through an independent feeder controlled by circuit breakers of sufficient rupturing capacity to handle the short circuit current of the system. The motors are designed for full-voltage starting. Thus space and cost saving is realized by utilizing the feeder circuit breaker as the motor starting switch. Such switchgear must be of rugged construction, capable of continuous operation and of frequently starting a fully-loaded mill which requires a starting torque of approximately 175 to 200 per cent. This equipment is provided with remote control stations conveniently located on operators' platforms throughout the mill length, the switchgear for it being located in a separate air-conditioned control room whereby maintenance is easily carried out and the equipment is protected against fumes, moisture, and dust. Each of the switchgear cubicles is equipped with relaying equipment, varying somewhat with the purpose of the individual feeder. Protection for the ball mills consists of two sets of induction relays which give protection against feeder or motor short circuits, starting and accelerating, and continuous overloads. A typical graph showing the protective relay curves in this application is shown in Figure 6. Comparison of this curve with those of Figure 5 indicates the difference between the two types of protection. In the latter case the service is such that motors are no subject to overheating from repeated attempts to start under locked rotor conditions, therefore relays of the induction type, which reset instantly, give the required protection. It has been found advantageous to furnish power to such circuits from a Y-connected grounded transformer bank, particularly where outgoing feeders are also connected to the same bus. The advantage is that a sensitive ground relay can be added to each circuit-breaker panel, and as many faults start from wire to ground, much faster clearance is obtained than with the 3-phase protection alone.

All of the power distribution in the concentrator for motors 75 horsepower and larger is made at 2,300 volts and power is taken from the same busses to transformers for the 440-volt distribution. In a preceding paragraph reference was made to full-voltage starting and also to the high-starting torque requirements. For these reasons, care must be exercised in the design of the feeder and bus system, to provide for adequate carrying capacity under starting conditions. The system reactance must be considered also when calculations for voltage drop are being made, so that the total voltage applied to the motors during the worst starting conditions may be determined. This is particularly necessary when starting torques in the range of 150–200 per cent are required.

Passing to the flotation section, a great many individual drives are used on the mechanical agitation system, some 300 such motors ranging from 10 to 25 horsepower being required for a 25,000-ton plant. To conserve space and reduce the length of the launders, most modern concentrators are built with all of the equipment grouped closely together, leaving only adequate working space and walkways around the groups of machines. Because of the nature of the process a great deal of moisture is always present, and the electrical distribution, both 2,300 and 440 volts in these buildings, proposes a problem both from the standpoint of room and protection against moisture. All distribution is concealed in the walls, floors, or under floor ducts, and because of the huge quantities of water (approximately three to four tons of water to one tone of ore), being pumped continuously through the plant, adequate provision must be made against possible flooding of ducts and conduit runs. It is desirable therefore to eliminate all conduit runs which start and end at a higher elevation than the main body of the run, and to effectively drain all conduits and cable

trenches. Fortunately, because of the desirability of preserving slopes to aid the solution flow, each section is progressively lower than the preceding one and it is possible to provide trenches with adequate drainage for under-floor conduits and raceways.

It has been mentioned that large quantities of water are required for the flotation process. Approximately 75 per cent of this water is reclaimed and pumped back to the concentrator head tanks to be used again. This involves a number of large pumps with their driving equipment, about which there are no special features except the interlocking and level control systems which automatically pump the water in the required amounts. These controls are also equipped so that failure of one pump will close the starter of a second pump and thus pick up the load.

Smelting

From the filter plant the partially dry concentrate is transferred to the smelter where it is charged into gas, oil or coal-fired furnaces known as Reverberatories. At this point the necessary fluxes required for proper smelting are added, and as the charge is melted down, impurities form at the top of the bath which are later tapped and run off in the molten state as slag. The lower part of the bath is now molten metallic copper and is tapped off as matte into ladles and transferred to the copper converters for further treatment. At this stage the copper contains a large percentage of sulphur and iron. In the converters, air is continuously blown through the copper by a series of pipes, thus burning up most of these impurities. After the proper stage has been reached, the converters are tilted and the copper is poured out and transferred to what is known as a poling furnace, where wooden poles are inserted in the copper to reduce the oxygen content. At the proper stage, the comparatively pure metallic copper is poured into slabs or anodes for further refining.

Although the smelting process is an ancient one, modern production methods have made it possible to handle large tonnages continuously, and the application of electric power to this process is responsible in a great measure for the comparative ease with which enormous quantities of molten copper are handled daily, and for the safety features which allow the process to be carried on at maximum speed and with minimum interruptions. From the moment the molten copper is tapped from the reverberatory furnaces, it is handled by huge electric cranes until it is ready for shipment in the form of billets or anodes.

One of the most interesting features in connection with the electric-power supply to a smelter is that if either the electric-power supply or the air supply to the converters is interrupted, a serious situation immediately arises. As stated before, air is continuously blown through the molten-copper mass in the converters. This is introduced into the lower part of the converter at about 16 pounds pressure through pipes or openings called "tuyeres." Should the air supply fail, molten copper immediately runs into the pipes under the converter, and unless the latter can be tilted quickly the copper cools in the orifices and cannot again be melted out. Apart from the loss entailed by thus ruining the furnaces, the loss of production would be tremendous before new converters could be installed. Every effort therefore is made to insure continuous air supply, and failing that, continuous electric power for as long a period as is

Figure 6. Typical relay protection curves for 800-horsepower synchronous ball-mill drive

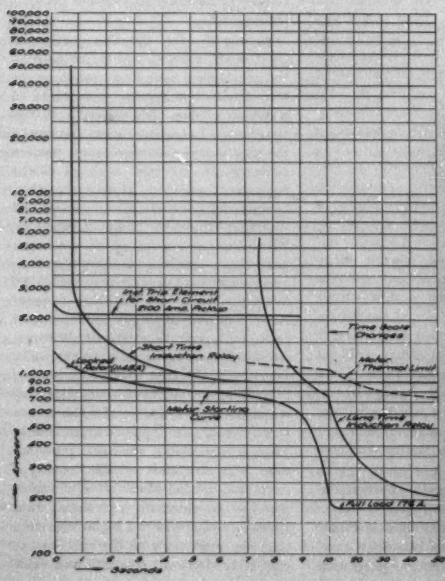


Figure 7 (upper right). The concentrator at Cananea

The main building houses the ore bin, grinding machinery, and flotation section

Figure 8 (lower right). View showing skipway with primary crushing plant at bottom, hoist house at top

necessary to tilt all the furnaces, thus uncovering the tuyeres and allowing the molten copper to run out before setting.

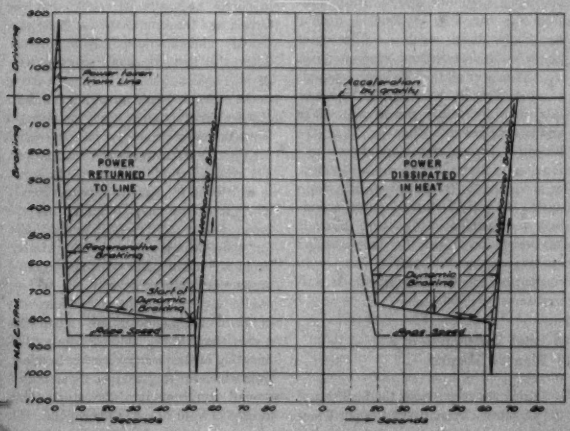
Several methods have been used to insure continuation of electric power. This is sometimes accomplished by the use of stand-by storage batteries, steam-driven auxiliary generators, or what is known as flywheel motor generator sets. The latter is one of the most interesting applications and has proved to be a very reliable piece of apparatus. Because of their flexibility and ruggedness, d-c motors are usually applied for operating the crane and tilting the converters, so that one or more generator sets are necessary to convert the power from alternating current to direct current. In the design for a smelter, after the final requirements are known, it is possible to calculate the total amount of energy required to tilt all the converters and to move all the cranes which may be handling molten metal, and by incorporating a suitable flywheel in the motor generator set this amount of energy, with a reasonable margin, can be stored mechanically. This is the principle of the flywheel motor generator set, and in practice, if failure of either or both air and electric power occur, this power is instantly available to turn the converters into a safe position and complete any other operations which are necessary under these conditions. In conjunction with the incorporation of the flywheel, specially-excited generators are furnished with voltage regulators designed to maintain full voltage down to as low as 50 per cent of the normal speed. Thus the stored energy of the fly-wheel is utilized while maintaining normal voltage. Control switchgear for the smelter is similar to that used in other parts of the plant except that special provisions are made and controls incorporated so that on failure of converter air, alarms are sounded and all nonessential feeder circuit breakers are automatically opened, thus leaving the energy stored in the flywheel for use on emergency services only.

The crane driving equipment is of extremely heavy duty and rugged design, with safety features intended to prevent damage from accidental or careless operation and to provide safety for the men working in the converter aisle below the ladles of molten copper.

The final application, which is probably peculiar to the copper industry, is the casting wheel drive. When the copper reaches the final stage, it is frequently poured in the form of anodes, molds for which are carried on a large circular wheel, approximately 40 feet in diameter. Continuous casting is achieved as the anodes are cooled by water sprays and removed before the wheel completes a revolution. As each mold is poured from the pouring ladle, which is kept continuously filled by tilting the poling furnace, the wheel is required to start up, turn a few degrees, and come to rest in position for pouring the next mold. As this operation is carried on rapidly, it is absolutely necessary that the wheel stop precisely in the right position, accelerate smoothly, move the required distance and decelerate smoothly to the next stop. Smoothness of starting and stopping is necessary in order to prevent disturbing the still molten copper as it cools during the intermittent movements required to complete a revolution. Control for the casting wheel drive is automatic and usually incorporates a variable voltage system with special means for achieving smooth acceleration and deceleration. Start and stop push-buttons only are located on the operator's platform, the control of acceleration, deceleration, and spotting being inherent in the control equipment design.

Figure 9. Torque diagram for lowering hoist

Cross-hatched area below zero power line represents power returned to line in regenerative lowering



Power Distribution

Where available, power for copper mining operations is purchased from public utility systems, but in many cases because of the remoteness of copper ore deposits, it is necessary to build and operate a power plant for the generation of electric power, so that frequently copper mining and milling operations have a complete electric generating and distribution system designed to meet the rather strict requirements of dependability and continuity of operation. In these systems the principles of sound electrical engineering are applied to obtain the desired results. The capacity of the generating plants range from 30,000 to upwards of 100,000 kw and generator voltages range from 2,300 to 13,800 volts. In some of the larger properties duplicate main busses and switchgear are incorporated in the design in order to insure continuity of operation. In addition, it is frequently desirable to lay out the distribution in sections so that the flow of power parallels the metallurgical flow sheet. By these means, serious interruption of power from any cause in one section will affect only a portion of the output instead of shutting down the whole plant. From the main power plant, parallel and adequate feeders are run to the main substations and other points of distribution where the voltage is transformed to that most suitable for the local needs.

In the type of plant described previously, by far the bulk of the power is distributed from the concentrator substation, where it is frequently advisable to divide power between two or more transformer banks in order to provide a measure of flexibility. In large plants it is advisable also to divide the main concentrator bus into one or more sections for the same reasons and in order to reduce the interrupting-capacity duty on the circuit breakers.

From the main outdoor substations, the secondary connections run to the con-

trol substation located in the concentrator building. Because of the large amount of power involved, it is advantageous to install these cable runs in ventilated tunnels to provide easy access and permit frequent inspection. Through these tunnels is also introduced the cooling air for the main indoor substations, thus providing adequate ventilation and cooling of the cable runs. The danger from fire is reduced by installing cable in transite asbestos conduits racked on each side of the tunnel. In addition, fire detection apparatus will instantly shut off the ventilating air in the event of a fire originating in the tunnels.

In the indoor substations, as mentioned before, are located the main switchgear for 2,300-volt feeders and also all of the magnetic starting equipment for the flotation motors and other auxiliary equipment. Because of the concentration of this control equipment, the utmost care must be exercised in properly arranging the banks of the 440-volt control equipment to provide adequate ventilation, accessibility, and ease of maintenance.

The main concentrator substation also frequently houses the feeder switchgear for other parts of the plant, such as the primary and secondary plants, drive houses, filter plants, and pumping stations, so that all of the secondary feeders, which comprise the main distribution, are under the control of one operator at a centralized location.

At many places where these mines are located, severe electrical storms are of frequent occurrence, and this section would be incomplete without a reference to the measures employed for protection against lightning. In many of the localities it has been found impossible to entirely protect the systems against momentary surges from heavy lightning strokes which cause disturbances ranging from tripping out the circuit breakers to very severe damage caused by the destruction of some part of the electric power system or equipment. For these reasons and in common with general practice on power systems, the best results are obtained by providing for very fast tripping of circuit breakers on those circuits which are exposed to lightning. Apart from the installation of adequate surge lightning arresters at every point likely to be affected, disturbances are minimized and better protection is obtained by using a grounded neutral system on secondaries of all transformers feeding the affected circuits. By this means sensitive ground relays can be installed providing instantaneous tripping which will open the circuit with the first discharge from line to ground. Practically all measures for lightning protection are of little value, however, unless adequate grounding is provided at all points where grounds are required for lightning arresters or other apparatus. In heavy lightning districts and dry areas, the practice of driving a few ground rods or carrying a cable down the pole and terminating same in the pole excavation is useless, and the protection obtained thereby is negligible. Damage sustained to equipment connected to distribution lines protected in this manner has been very severe, sometimes resulting in complete destruction of switchgear and distribution transformers. In certain areas of the southwestern United States and in the western portions of Mexico, the problem of obtaining adequate grounds is a real one, and in certain locations this can only be done by drilling deep holes down which a conductor of ample size is dropped and terminated in a suitable copper plate. To insure grounds of a resistance in the order of 10 ohms or less, it is frequently necessary to take additional measures such as filling the hole with charcoal and salt, and providing for a water supply to keep the area damp, or arranging for frequent watering down during the periods when electrical storms may be in evidence. By these means it has been found possible to reduce lightning outages and greatly restrict damage to equipment which was formerly experienced.

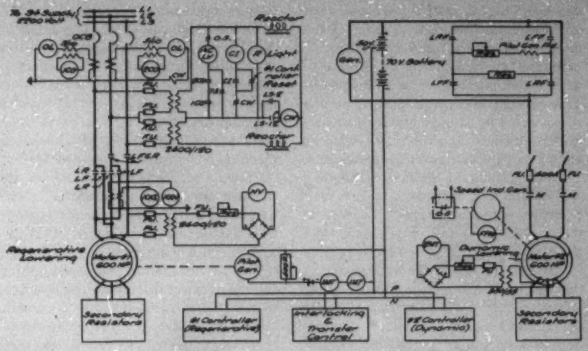


Figure 10. Diagram showing main connections of hoist motors

A Brief Description of a Plant in Mexico

During the recent war, the facilities of the Cananea Consolidated Copper Company plant located at Cananea, Sonora, Mexico, were expanded to develop for mining by open-pit methods, a low-grade ore body adjacent to the underground mines which have been operated for many years. In spite of the shortages of material and other difficulties imposed by the war, this low-grade mine was stripped, opened, and the plant constructed in the years 1943–44. A modern concentrator was built, together with crushing and auxiliary plants, the whole constructed entirely by Mexican labor, the mine and plant starting production in late October 1944. This mine and plant were designed for a daily tonnage of 12,000 tons of ore and because of some of the unusual features encountered, a short description may be of interest to the profession.

Cananea, Sonora, is located about 45 miles by road south of the border town of Naco, Arizona, on the east slope of the Cananea mountains. The climate is moderate and very agreeable, but the terrain and location of the ore body presented a difficult transportation problem. After the first locations selected were decided against because of faults existing in the rocky formation, making it unsuitable for the erection of heavy buildings, a site was finally chosen for the concentrator about 500 feet below the mine proper. The completed mill is shown in Figure 7, in which the filter plant appears directly in front of the main building. In the foreground are 250-foot thickener tanks, for the purpose of reclaiming water. From the filter plant the concentrated product is trammed over to the existing smelter and converted to metallic copper in the manner just described. Figure 8 is a view showing the inclined skipway terminating in the primary crusher building, from which ore is discharged by an underground conveyor into the storage bin located in front of the primary plant. In

Figure 11. Concentrator control board

Main 2,300-volt switchgear cubicles

the right-hand lower corner appears the secondary plant, with the conveyor gallery leading from underneath the storage bin. In this same figure, the mine benches appear in the upper left-hand corner.

At this property the problem of transporting the ore from mine to mill presented great difficulties. Because of the rough terrain and the difference in elevation, trucking or railroad haulage was almost out of the question because it would have been necessary to traverse switchback roads over the mountain face. Some of the approach roads necessary for operation of the plant appear in Figure 8. The problem was solved by a special hoisting application in which the functions of the ordinary hoist are reversed and it becomes a lowering operation. This hoist is known as a skip hoist and utilizes two 30-ton skip cars which operate on heavy rails having a gauge of 10 feet. The skipway is constructed down the mountain side with a total length of 1,000 feet and with an incline of approximately 30 degrees. The hoist operates in balance, one car being at the top of the slope while the other is at the lower level. The skipway is provided with three loading stations above the primary plant, and the procedure is to haul rock from the level on which it is being mined to the loading station on the nearest level. Twenty-two yard trucks were used, each carrying a load of 30 tons. After loading the skip it is lowered to the primary crushing plant while the empty car is pulled up on the opposite track. During dumping of the first load the second skip is loaded and the cycle repeated. The hoist system was designed to handle a maximum capacity of 1,080 tons per hour, which assured sufficient capacity to deliver the average daily tonnage in less than two shifts. The skip was required to complete the cycle in 100 seconds. Actual length of run from the top loading station into the dump pocket is 750 feet and the rope speed 860 feet per minute.

A choice of method of lowering was available, the two most attractive for loads of these proportions being regenerative or dynamic. In such installations it is usual to provide a spare motor on important pieces of apparatus which form a single link in the process. In the installation referred to, as both regenerative and dynamic lowering had attractive features, a combination control was designed which is unique in many respects. The hoist was provided with two driving pinions, and the spare motor instead of standing idle was mounted coupled to the hoist at all times and equipped with dynamic braking controls. Normal operation is conducted by the use of the number 1 motor which is regenerative. This has a double advantage in that after the hoist has once been started, the descending load rapidly overhauls the motor and at slightly above synchronous speed power is generated and returned to the line. An additional advantage is that the descending load operates at a constant speed as long as the motor controller is in the full on position, whereas with dynamic braking, manipulation of dynamic controller is required at all times to adjust the dynamic-braking torque to the speed of the rope. On reaching the bottom of the skipway, the skips are dumped by a hydraulic mechanism operating at the side of each track, dumping the ore into the center onto the inclined grisly from whence it slides by gravity into the primary crusher. Because of the necessity for accurate stopping, without loss of time, it was advantageous to stop the skip against solid buffers. Therefore no overshooting is permissible, and a feature of the installation is that safety controls will automatically stop the skip if the operator fails to reduce its speed within a safe stopping distance before reaching the pocket.

A curve showing the torque requirements appears in Figure 9. Both regenerative and dynamic lowering cycles are indicated. In the former case, power is taken from the line for fast acceleration and returned to the line after synchronism is reached, the motor then acting as an induction generator. In the case of the dynamic braking cycle, however, the hoist is accelerated by gravity and a longer time is required. The power generated in lowering is not returned to the line but is dissipated by a bank of resistors.

Referring again to the main driving motors, it is obvious that loss of electric power or failure of any of the control mechanisms would result in the skip running away unless the operator was able to stop it by means of the mechanical brakes before the loaded skip crashed into the primary crushing plant. To guard against this possibility in the case of failure of the regenerative motor, whether due to loss of power or failure in the motor or controls, automatic energizing of the dynamic lowering motor immediately takes place, bringing the hoist to a halt in a very short distance. This second motor is a duplicate of the first or regenerative motor, except that one phase of the primary winding is energized from a storage battery of ample capacity to provide dynamic braking functions. In addition to the usual hoist controls an additional controller was provided, whereby in case of a prolonged outage on the power line, the skip could continue to operate as a dynamic lowering hoist, although at a somewhat longer time per cycle. The schematic diagram of main connections is shown in Figure 10 and a brief description of the functions follows.

As stated previously, safety features to provide against almost any contingencies are included. Referring to Figure 10, starting is accomplished by advancing number 1 controller from the off position, which closes the forward contactors designated *LF*. The controller is equipped with eight positions, the last of which is the full-speed or full-torque position. To avoid extremely heavy electrical or mechanical shocks, the controller is therefore notched up as the hoist accelerates until in the full-open position maximum regenerative torque is obtained. Should the operator fail to advance the controller to the full-open position by the time the hoist has reached synchronous speed, a runaway would be liable to occur because of insufficient torque to restrain the descending load. However, motor number one is provided with a pilot generator which generates a voltage proportional to the speed. Through a rectifier indicated

in the circuit diagram, its voltage is opposed to that of the storage battery through voltage differential relay coils *IMT*. At about 95 per cent synchronous speed the pilot generator neutralizes the battery voltage and causes relays to drop out, which compel the secondary contactors into the full-torque position, thus restraining the load to a descending speed corresponding to "slightly above" synchronism. Adjustment of the drop-out point is made by varying the resistor across the generator output.

In the event of failure of line voltage, relay *ACLV* loses its energizing power and through the contactors located in the interlocking and transfer control cabinet initiates closing of the dynamic braking contactors *M* causing the dynamic lowering motor to take over at a predetermined restraining torque, holding the hoist to a low speed until the operator takes control by means of the dynamic lowering controller. As stated previously, both motors 1 and 2 are permanently geared to the hoist. The speed indicating generator, which is driven from the latter, is also equipped with overspeed contacts. In case of excessive speed, these contacts are opened and in turn open the circuit of the low-voltage relay *ACLV*. When this occurs, by means of interlocks, not only dynamic braking is applied but also the mechanical brakes of the hoist are applied.

Although the function of the hoist is essentially lowering, motor number 1 is equipped with forward and reverse control to permit moving the skips against the grade in case the necessity arises and to permit cable length adjustments. The hoist is also equipped with a standard Lilly overspeed governor, but because serious damage and loss of life might occur should the hoist run away, a further safety feature is incorporated. This is accomplished by means of a so-called "speed trap" installed on the trackage. At the point where deceleration of the load should be well under control, if for any reason the operator has failed to reduce speed when approaching the dump pocket, the design of the speed trap is such that this is detected within a few seconds and emergency dynamic braking is applied. If the speed of the hoists is still too high on emerging from the speed-trap zone, mechanical braking is also applied to the hoist, the combined effect of the dynamic and mechanical brake rapidly bringing the skips to a stop. In addition to the foregoing an emergency stop button is located in front of and within easy reach of the operator. In case of emergency it is only necessary for him to give it a light blow to set all emergency braking. In case of cable breakage the skip will no longer be under control but will be stopped by fast-acting clamp-type brakes which grip the rails. These brakes are operated by release of the cable tension.

Distribution in the concentrator was made in a manner similar to that outlined previously under "Concentrating." The main indoor substation was located under the ball-mill floor on a level with the flotation floor, ample room being provided for operation and maintenance of all the equipment housed therein. Completely enclosed metal-clad switchgear was used in all of the substations. Part of the concentrator control board is shown in Figures 11 and 12. A view of the ball mill section of the concentrator appears in Figure 13. One of the 800-horsepower synchronous motors appears at the left-end of the spiral classifiers. In this illustration can be seen also the inclined conveyors carrying the ball mill feed which is controlled by automatic conveyor scales which register the weight and control the rate of feed as it passes over the conveyor. Control for the ball mills and other drives on this floor are located on the operating platform at the rear of each ball mill. Note the absence of power wiring or switchgear in this area where all available space is required for access to and operation of the grinding equipment.

The concentrator substation is located directly below the ball mill floor. Power for the concentrator is brought from the outdoor substation through a tunnel which runs behind the control room. This tunnel serves for distributing power to the ball mills, classifiers, and so forth, located on this floor, and in addition is used as the main air duct for ventilating and cooling the control room in which are housed the d-c motor generator sets as well as the main and secondary switchgear. Proper and adequate ventilation of control rooms, particularly where located in centers of buildings, is necessary for this type of plant, and is a feature of modern installations. In addition to carrying away the heat generated by the rotating equipment, transformers, and so forth, it serves the purpose further of keeping out the moisture laden air, which is always present in the grinding and flotation sections. The main control room is

Figure 12 (left). One end of concentrator control board showing part of the 440-volt distribution board and motor-generator sets for crane operation and synchronous motor excitation

To avoid disturbances in the latter circuit the two d-c busses are normally segregated

Figure 13. View of ball-mill section

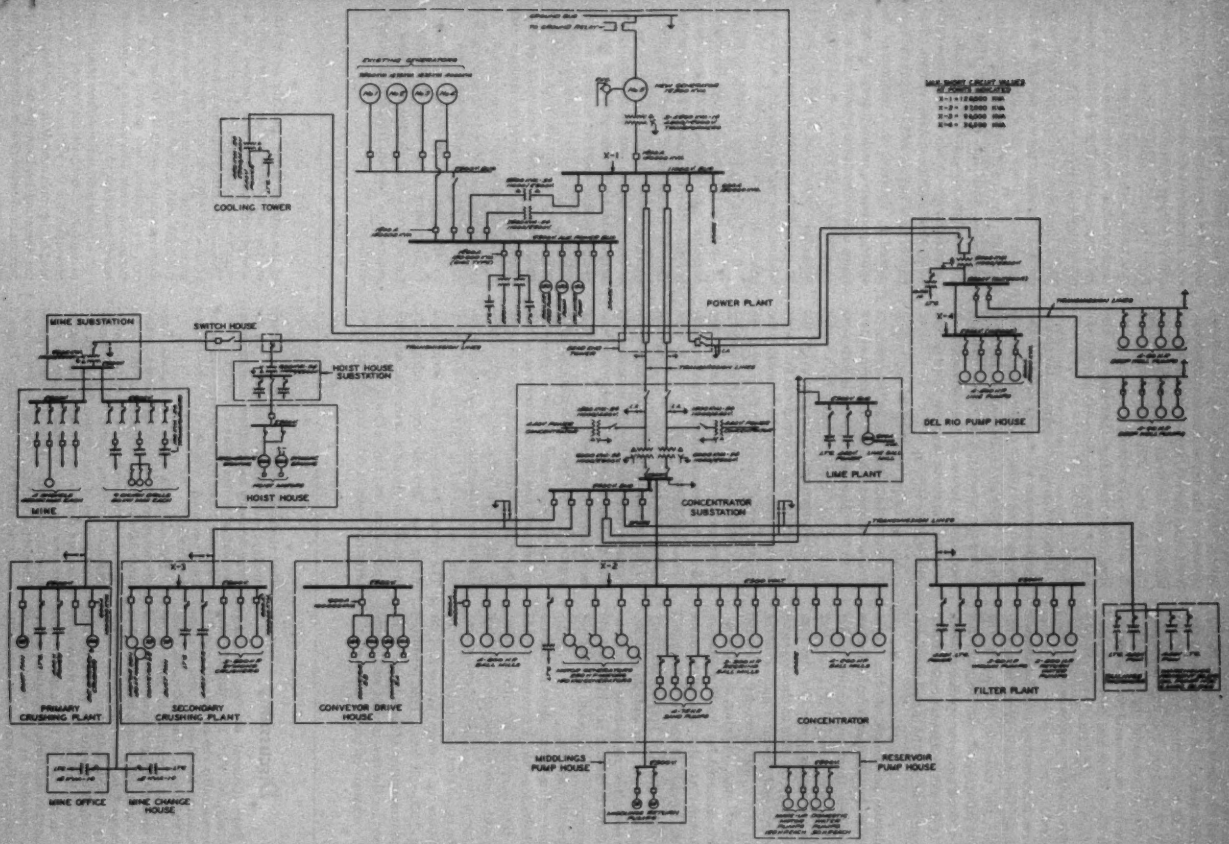


Figure 14. Power distribution one line diagram of main connections

ventilated by a 25,000-cubic foot blower which maintains a positive pressure in the room at all times, thus preventing the entrance of moisture and dust-laden air. It serves a further useful purpose in that warm air from the control room is forced through the conduit and duct runs emanating from the substation, thus tending to disperse moisture which would otherwise collect.

With reference to the power distribution as a whole, this was designed in accordance with the methods outlined in the preceding paragraphs, with the exception of a few departures for special reasons. Figure 14 shows the schematic diagram of the power plant and the distribution system.

It should be noted at this point that power for the previous operations was supplied from an existing power plant which was however of insufficient capacity to supply power for the low-grade ore project. Due to the exigencies of the war, a new generator was out of the question and it was necessary to extend the power house, utilizing a 10,000-kw generator allocated by the War Production Board. As this generator alone could not furnish all the power required for the new project it became necessary to interconnect the new and old systems. In any case, this would have been desirable because in the case of a failure of any unit, emergency power could be obtained from the other system.

Because of inherent short-circuit capacity limitations, the new plant bus could not be directly connected with the existing one and a limited link was established by the use of two 2,500-kva transformers. It will be noted that the auxiliaries for the new unit are fed from an extension of the existing 2,300-volt bus. This permitted the maximum output of the new generator to be used for the new project with a very limited exchange of power between the two systems. Because of the unusual voltage of the turbogenerator allocated, it was necessary to incorporate a step-up transformer between the generator and the 11-kv bus. This, however, served the purpose of increasing the system reactance, thus reducing the duty on the main oil circuit breakers throughout the plant.

Indicated on this diagram are the short-circuit currents at various points in the system and these were adjusted by proper choice of transformer impedances, so that the capacity of the circuit breakers used on the busses in the substations is of lower rupturing capacity than would otherwise be required.

An interesting result of the application of regenerative lowering to the hoist described before is the fact that from 30,000 to 40,000 kilowatt-hours per month were returned to the power house in excess of the hoist demands. This resulted in the transportation of the ore from the upper skip level into the surge bin preceding the secondary plant at no cost for power. In other words, the crushing and conveying of the ore up to this point was accomplished by power derived from the operation of the hoist.

From the power house, bus feeders at 11,000 volts are carried by overhead lines. Some of these, notably the mine and Del Rio feeders extend a considerable distance and are exposed to severe electrical storms. In the case of the main pumping station located some 13 miles away, a 2-circuit line was installed because of the importance of a continuous water supply. In addition to other measures to insure reliability in operation considerable attention was devoted to lightning protection. The design for the 11-kv line included a static or ground wire, adequately grounded at frequent intervals, and the installation of high-capacity lightning arresters both at each end and at intermediate points. That this protection is justified is shown by the excellent performance compared with lines without static wire or properly rated lightning arresters. From the main pumping station, 2,300-volt feeders are carried in opposite directions to eight deep well pumps distributed along the bed of the Del Rio, extending about two miles in each direction. These lines were similarly protected with surge lightning arresters at each pump house.

Similar construction was used for the mine line, with the exception that in places where the greatest exposure appeared likely, double static wires were used, this measure having proved effective in such terrain and reducing outages caused by lightning to a minimum. As stated previously in this paper, the importance of low-resistance grounds cannot be overstressed, and the co-ordinated lightning protection system as a whole resulted in these lines continuing to give service when others not so protected were incapacitated.

It will be noted by referring to Figure 14 that the transmission lines emanating from the 2,300-volt bus at the concentrator substation are operated from grounded neutral transformers, thus obtaining a quick clearance on ground faults.

In conclusion, an interesting point about the 12,500-kva generator is that ground protection of its windings and the primary of the step-up transformer could not be obtained by the usual resistance method of grounding because of the large physical size and cost of the resistors with a machine of such a low voltage. In consequence, a modified potential method of grounding was used, which is designed to trip the main-generator oil circuit breaker after a very short time delay at the start of a fault to ground in either the generator or step-up transformer primary windings.

Discussion

R. S. Sage (General Electric Company, Schenectady, N. Y.): The comprehensive paper presented by the authors is a notable contribution to the literature now in existence on this subject. The interesting treatment of various operations involved in the modern open-pit copper mine should be of interest alike to the mining industry and to those engaged in the application of electricity to this basic industry.

The gradual substitution of electric and other forms of haulage for the steam locomotive in several of the large open-pit operations has resulted in operating benefits scarcely matched in any other field of modernization. For example, it is reported that the recent substitution of the electric trolley type for the steam locomotive by one large company has resulted in an increased ore output of more than 20 per cent. This has come about by the greater availability and increased reliability of the electric locomotive.

For essentially the same reasons, large operating economies and increased tonnages have followed the replacement of the steam shovel by the modern d-c drive caterpillar-mounted revolving shovel. A not insignificant portion of the benefits derived in this case are due to the greatly reduced maintenance expense. Reliable figures in some quarters indicate a ratio of 4:1 in favor of the electric shovel. It may be well to point out, incidentally, when referring to Figure 2 of the authors' paper, that the series fields on the adjustable voltage generators are wound differentially with respect to the separately excited and self-excited fields. This combination results in a characteristic admirably adapted to a machine of this nature in that the dipper may be stalled in the bank with a minimum of shock to the shovel structure and to the driving machinery, by limiting the current which the motors can draw, to a definite and predetermined maximum value.

It is important, as the authors pointed out, to carefully co-ordinate the type of overload protection for the various machine drives, used in the concentrator, in accordance with the operating load conditions, so that the maximum of protection is afforded without

unnecessary removal from service by the tripping of overload relays.

The authors have well presented the advantages of d-c dynamic braking for the ore skip hoist. There seems to be a revival of interest in this type of control for wound rotor induction motors which operate hoists involving a considerable amount of operation with overhauling loads. The economies realized in power consumption and the ability to operate satisfactorily at reduced speeds, when handling heavy down-going loads, justify serious consideration to this well known, but not too frequently applied, control system. While especially adaptable to unbalanced slope hoists, this type of control has also recently been applied to very deep balanced shaft hoists, in which the down-going rope presents a heavy negative load, not otherwise effectively contended with where a wound-rotor drive motor is used, unless plugging is resorted to.

Victor Siegfried (American Steel and Wire Company, Worcester, Mass.): This paper is very interesting and presents a clear concept to the uninitiated of the electric equipment involved in mining uses. From the stand-point of a cable manufacturer, I am very much interested in the usage or load factor of the equipment on the shovel fed by trailing cables.

The current carrying capacity of these trailing cables involves a reasonable guess as to the likelihood of some of the equipment not being in use at the same time as other. This is indicated in part by the rating of the motor generator set, with a motor of 275 horsepower and connected generator capacity of 240 kw or 320 horsepower at 100 per cent efficiency, in the example cited. This infers a load factor of approximately 70 per cent if conversion efficiency is 90 per cent. A growing tendency to use smaller cables for economy of cost or size or for greater storage capacity of cable on the reels indicates need for caution in the application of such a load factor. It is a matter of concern lest cable designers and equipment designers use the load factor in an optimistic manner and use too small cable for the actual loads encountered, or require cables to run hotter than is intended. The cable industry would like to have the authors' estimate of a good load factor figure for open-pit mining operations.

S. F. French and H. Speight: This will acknowledge the comments presented by R. S. Sage, in which the writer has ably pointed out several interesting phases which were not covered by the authors.

It is our assumption that in the paragraph in which reference is made to d-c dynamic braking for the ore skip hoist, Sage also intends to include the application of regeneration for negative descending loads. In the specific application cited, both regenerative and dynamic braking are used in normal operation of the hoist although the authors omitted this explanation in the original article. It will perhaps clarify the matter to state now that at the moment the controller is moved to the off position, a percentage of the total dynamic braking available is automatically applied, reducing the duty on mechanical brakes and retarding the descending load until it is further slowed by the operator.

In his discussion, Siegfried brings up some questions which introduce problems involving many factors, and because of this it is difficult to present the answer in a brief and concrete form. The question of cable ratings for such application is of a very controversial nature and it is doubtful whether a fixed load factor can be assigned to suit all conditions of operation. To illustrate the range of current values that may be obtained on the type and size of equipment, of which the illustration is typical, the motor generator set, through which power is furnished to the shovel is rated 275 horsepower, 220 volts, 3 phase. Full load amperes of this motor are 64, with a locked rotor value of 460, and it is obvious that values of current between the no-load value and the locked-rotor value may be imposed frequently with the shovel digging in rock. The characteristics of the hoist and crowd generators, however, are designed to limit the torque output and therefore the load which will be imposed on the motor and line. In practically all cases the greatest load will come in hard digging, when both the hoist and crowd motors are near their stalling points. In both instances we can assume a maximum torque on the hoist and crowd motors of approximately 200 per cent rated torque, which at 85 per cent combined efficiency would require approximately 530 horsepower from the motor, and this value can be taken as the maximum short-time peak, as no other motions are involved under these conditions. Assuming voltage to be maintained, the motor amperes will rise to approximately 145. Neglecting the consideration of voltage drop which may or may not be involved, depending on the system, conservative practice would indicate that a number 2 rubber-insulated rubber-sheath 3-conductor cable should be used. This cable has a rating of approximately 88 amperes continuous in air with a copper temperature of 60 degrees centigrade.

The foregoing is, of course, based on 40 degree ambient, and if conditions are such that higher air temperatures are frequent, proper allowance must be made. It will be noted that such a cable would have a continuous rating of about 135 per cent of rated motor capacity. Although the stalled digging current may be frequently attained, all equipment of this type is subject to alternations between the heavy loaded and the no-load condition, and such a cable as described, employing one of the high-grade rubber compounds will be found to give excellent service.

As stated previously, no general rule can be made, but each application must be considered individually because of the varying factors, such as: temperature, type of service, and mechanical abuse to which the cables are naturally subject. We believe the general tendency to use a smaller cable is largely due to another factor, namely—the greater convenience and flexibility of the smallest cable which will provide trouble-free and economical use.

Electrification of Petroleum Pipe Lines

M. A. HYDE
MEMBER AIEE

THE ADVANTAGES of pipe lines for the overland transportation of petroleum and its refined products, while known to the industry for many years, were most strikingly demonstrated during World War II. In that critical period of transportation shortage, existing and new lines formed a vital part of an integrated transportation system to make petroleum available where needed. During the years 1941 to 1945 inclusive, the mileage of trunk crude oil and products lines in the United States increased by about 13,000 miles, to a total of 85,000 miles, not including some 57,000 miles of gathering lines.

Naturally, some of these facilities have undergone modification to meet changed postwar needs. Among these are the 24-inch crude line and the 20-inch refined-products line, familiarly known as "Big Inch" and "Little Big Inch."[1,2] These two lines delivered as much as half a million barrels per day of crude oil and refined products from Texas to the New York and Philadelphia areas. They did this with electric power, requiring only $0.05 worth of electric energy to move each barrel over the 1,400-mile course. These lines are now in service as natural gas carriers. They are being equipped to deliver 433,000,000 cubic feet per day of much-needed Texas gas to the eastern areas. And in the performance of this task electricity will play a preponderant part. For driving centrifugal compressors on this project, 1,250-horsepower motors available from the 20-inch products line will be supplemented by larger motors now under construction, to a total of at least 117,000 horsepower in electric drive.

With all the additions made during recent years present pipe line facilities are inadequate. New lines are planned years ahead and their construction is proceeding at a rate limited only by the availability of steel pipe.

Table I, which summarizes an estimate[3] of the pipe line construction program for the 1947–1951 period, shows that projects completed, under construction, and announced, will add to existing pipe line facilities a mileage equivalent to more than once around the world.

There is a significant increase in the use of pipe lines for the transportation of petroleum products from the refining centers to important marketing areas. Numerous products lines have thoroughly demonstrated this method as the economic way of handling large shipments of liquid petroleum products on overland routes to centers of stabilized demand. The result has been consistent growth in such facilities and it is not difficult to envision future distribution of refined products by pipeline to every sizable inland community.

Advantages of Electric Power

In recent years pipe-line projects in the United States have shown a marked trend toward electric power. This is due to constantly increasing availability of electrical service and to widening appreciation of the advantages of electric power from the standpoints of low investment in station equipment, low maintenance and attendance costs, reliability, and effectiveness of control.

Motor Applications

The pumps used on crude oil and products lines are of both reciprocating and centrifugal types. Reciprocating pumps may be driven by squirrel-cage induction motor through gear or other means of speed reduction, or by low-speed synchronous motor applied directly to the pump-pinion shaft.

Except when pumping high-viscosity oils particularly through smaller lines, centrifugal pumps are used predominantly for crude and products lines, because of lower initial cost, smaller building space requirement, mechanical simplicity and inherent limitation of the pressure developed in the unit. Drive is ordinarily by squirrel-cage induction motor, coupled, at 1,800 rpm or at 3,600 rpm on 60-cycle power supply. In selecting the speed it is necessary to consider the combined cost and performance of motor and pump for the specific requirement. The pump may be less expensive at 3,600 rpm but the opposite is usually true for the motor.

In gas transmission, the use of centrifugal compressors driven by squirrel-cage motor is becoming more common with the development of suitable compressors. The major installations to date operate at 3,600 rpm with coupled drive. Step-up gear drive to compressor speeds above 3,600 rpm have been used in other applications and are a future probability in gas transmission.

The applications just outlined seem at first glance to be so simple as to warrant no further discussion. That would be true if pipe lines were always operated at maximum capacity, at uniform capacity throughout the line length, handling fluids of constant viscosity and specific gravity. For these conditions the constant speed motor would be ideal. But in crude and products lines, particularly modern lines with no "breakout" tankage at the booster stations, problems are introduced by operation at part capacity, by deliveries made at intermediate points, by variations in the pressure-flow characteristic of the line due to change in viscosity of the pumped liquid, and by

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M. A. Hyde is industry engineer, Westinghouse Electric Corporation, East Pittsburgh, Pa.

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variations in operating pressure when handling liquids that differ in specific gravity and viscosity.[4]

To meet these variations in operating conditions, and at the same time to hold the pressures along the line within the limits established by economic design, imposes the necessity of controlling the station output pressures. Speed control of the pumps is an obvious answer, but by and large it is not the economic one.

Line Operation at Partial Capacity With Constant Speed Pumps

Operation at partial capacity is practical with constant speed motors throughout the line, if the number and size of pump units is advantageously chosen. Usually the pressure to produce the designed flow through the line is applied at several stations along the line rather than at a single station at the source. The total applied pressure is the sum of the several station pressures, and the latter are held to values economical from considerations of pipe and equipment design.

Consider a pipe line over level terrain, with four stations, *A*, *B*, *C*, and *D*, each consisting of a single constant speed pump developing, at full flow, pressures *A1A2*, *B1B2*, *C1C2*, and *D1D2* as shown in Figure 1. The pressure gradient line between stations is *A2B1*, *B2C1*, *C2D1*, *D2E1*.

Now consider operation of this pipe line at reduced capacity. The total pressure applied to the line varies approximately as the 1.85 power of the flow, and the hydraulic horsepower as the 2.85 power of the flow. Therefore in a period when the line is required to transport less than full capacity, it is most economical to operate continuously at the lowest flow that will deliver the required throughput over the period, rather than to operate at some higher rate for a portion of the period and shut down for the remainder.

Figure 2 shows a typical pressure-flow characteristic for the pipe line represented in Figure 1, along with typical pump characteristics for the four identical stations operating in series to deliver 100 per cent pressure, resulting in 100 per cent flow. Shown also is a line representing the approximate power consumption at various flows for each pump operating.

The pipe line may be operated at reduced capacity by shutting down stations *B* and *D* leaving stations *A* and *C* in service, operating at a flow of 73 per cent and pressure of 54 per cent. Stations *A* and *C* are now operating at slightly higher pressure than they did when the line was carrying full flow. The pressure gradient in Figure 1 becomes *A2C1*, *C2E1*. The power per barrel per day is $25 \times 86 \times 2/73 = 59$ per cent of that at full flow.

Table I

Project Location	Type Lines	Mileage
United States	Crude oil	7,600
	Products	5,300
	Natural gas	10,900
Outside United States	All	5,200
Total		29,000

A still lower flow rate may be obtained by leaving only station *A* in service, operating at 52 per cent flow and 28 per cent pressure, the pressure gradient in Figure 1 becoming then *A2B1*. The power per barrel per day is $25 \times 75/52 = 36$ per cent of that at full flow.

In practice, ability to operate on these or any partial-capacity gradients is contingent upon the gradient line clearing any local high points in the pipe line. It is also necessary that the maximum pressure at the discharge of any station be within the allowable working pressure established for the pipe, and that the suction pressure at any station be above the low limit established for safe pump operation.

Suppose now that station *C* is out of service and it is desired to operate the line at maximum possible capacity. This can be done by operating stations *A*, *B*, and *D* as shown in Figure 3. At station *B* excessive line pressure is avoided by throttling pressure *B2B3*, also at station *D* throttling is introduced to dissipate excess pressure *D2D3*. The total pressure effective on the line is then equivalent to that of two stations with resulting flow rate and gradient the same as for Figure 1.

Even throttling half the pressure at two stations, the power per barrel per day is $25 \times 86 \times 3/73 = 88$ per cent of that for full capacity operation, compared to 59 per cent for the same flow when stations *A* and *C* are operated without throttling. Of course, power is only one component of the total cost per barrel transported, and the most economical operation is not determined by the minimum power per barrel.

It is evident from Figure 3 that if each station consisted of two pumps in series, then stations *A*, *B*, and *D* could be operated at the same flow rate by taking one unit out of service at stations *B* and *D* instead of throttling the excess pressure at these stations. Subdivision of the total stations pressure into two or more series units also provides operating steps intermediate to those obtainable by "skip station" operation.

This practice has been extended to the employment of as many as three main units of identical characteristic, plus one or more units handling the station throughput at reduced head. A 22-inch crude oil line under design at time of this writing will employ six stations consisting of three full-head units each driven by a 1,250-horsepower squirrel-cage motor. Each pump develops approximately 240 pounds per square inch under rated flow conditions, or 720 pounds per square inch for the three pumps in series. Each station will also include a half-head unit to develop 120 pounds per square inch handling full station flow. Three full units will operate normally, but at times two full units and one half unit will be used, or other combinations. This arrangement makes available seven pressure steps per station, using constant speed motors.

A similar arrangement of constant-speed pumps on a refined products line employs three full-head pumps each driven by a 200-horsepower motor and developing 320 pounds per square inch, in combination with a half-head unit developing 160 pounds per square inch, thus providing seven steps of station pressure with a maximum of approximately 1,120 pounds per square inch at a throughput of 18,600 barrels per day of gasoline. In this case the half-head unit consists of two pumps in parallel, each driven by a 60-horsepower motor. At rates below 12,000 barrels per day, the 60-horsepower units are used in series combinations for more efficient operation.

It is evident that numerous combinations of full-head and part-head units can be laid out to give multiple steps of station pressure. For a given total station capacity there is an economic limit to the extent of subdivision, as reflected in increased total cost of pumps, piping, valves, motors, and control. More than five pumps per station would probably not be justified in any case. The number of different ratings should preferably not exceed two for a pipe line having uniform capacity throughout its length.

With three pumps per station in two ratings, five steps of station pressure can be obtained by installing two full-head pumps and one half-head pump, all operable in series, as shown in Figure 4.

Ten steps of flow may be had by operating these stations in pairs with the pumps scheduled to give pressure steps of one half-head unit per pair of stations. Thus full flow is attained with $2^1/_2$ units in service at all stations. For the first step of flow reduction, stations 1, 3, 5, and so

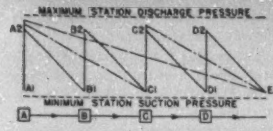


Figure 1. Pressure gradient for a pipe line with four stations on level terrain

forth, operate $2^1/_2$ units while stations 2, 4, and 6, and so forth, operate two units, an average of $2^1/_4$ units per station, under which condition the effective head is 92 per cent and the flow 95 per cent. The second step of flow reduction is with all stations operating two units, and so on down.

Under this schedule the first step of flow reduction imposes a pressure slightly higher than the normal pressure for 100 per cent flow at stations 1, 3, 5, and so forth, where all pumps are in service but at less than full flow.

The amount of excess pressure is minimized by installing pumps having a relatively flat-head capacity characteristic. It is usually feasible in pump design to hold the pressure rise within 20 per cent from rated flow to shut off. With such a pump characteristic the 10-step schedule outlined before involves a maximum pump pressure of only 103 per cent full flow pressure, this pressure being developed in stations 1, 3, 5, and so forth, and on the first step of flow reduction.

It is evident from these cases that constant-speed pump drives can be adapted to a wide range of line flow rates, providing small increments of flow, and requiring little throttling action.

It is always desirable to operate with minimum throttling, since the throttled pressure represents a proportionate part of the pump input power that is not utilized in the line. However, the station throttling valve under suitable automatic pressure-limit control has been a very useful feature on many pipe lines. The control system commonly employed automatically introduces throttling to prevent a rise of the station discharge pressure above the established upper limit, and introduces throttling to prevent a fall of the station suction pressure below the desired minimum. This control system is also useful for bringing a pump onto the line smoothly. The oncoming unit can be started with the control system set to maintain the previously existing pressure at the station discharge, and then by slowly increasing the discharge-pressure setting, the pressure increase can be made effective on the line very gradually with a minimum of pressure surges.

In shutting down a unit, smooth line conditions are attained by first gradually reducing the station discharge-pressure control setting to the value anticipated without the unit, and then stopping the unit. Automatic pressure-limit control by throttling has contributed on numerous lines, both product and crude, to smooth operation with constant speed pumps, and has prevented many interruptions that would have occurred due to

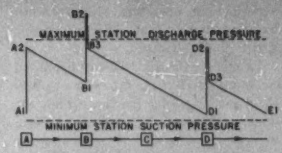


Figure 3. Pressure gradient for 4-station pipe line with third station out of service

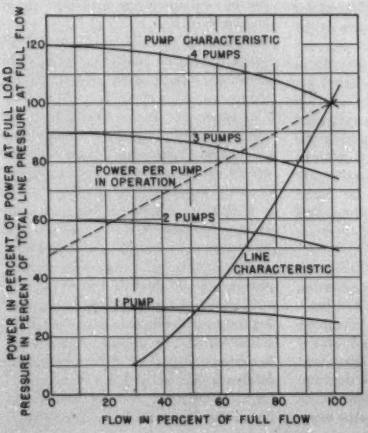
Excess pressures throttled at stations *B* and *D*

development of pressures beyond the settings of protective shutdown devices.

Constant Speed Pump Operation on Liquids Varying in Viscosity

When the pipe line must handle liquids varying in viscosity, the pressure changes encountered with constant speed motor drive are minimized by using pumps having relatively flat-head capacity characteristic in the normal operating range. Figure 5 shows a typical pump characteristic (*F*) in combination with the pressure-flow characteristic for average operation with oil of viscosity 70 Saybolt universal seconds (SUS) at 70 degrees Fahrenheit, also for viscosity 52 SUS representing summer conditions and for viscosity 115 SUS representing winter conditions. With the comparatively flat pump curve shown, the pressure variation (*a*) is only from about three per cent above to two

Figure 2 (left). Pressure-flow curves for pipe line with four stations, each containing one centrifugal pump



PUMPS IN SERVICE	EFFECTIVE HEAD IN PERCENT OF HEAD AT FULL CAPACITY	FLOW IN PERCENT OF FULL CAPACITY
2-1/2	100	100
2	83	90
1-1/2	65	79
1	45	65
1/2	24	45

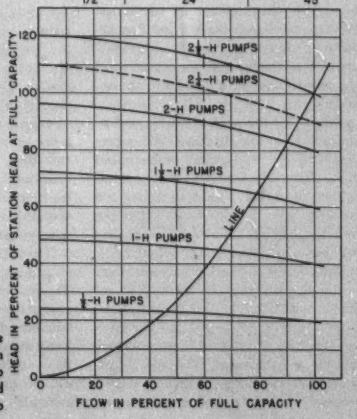


Figure 4. Pressure increments for station consisting of two full-head pumps and one half-head pump

per cent below the pressure corresponding to 100 per cent throughput at average viscosity, a range of approximately five per cent. A steeper pump curve such as *G* would have resulted in increased pressure range (*b*) approximately 11 per cent between the same limits of viscosity.

For simplification in Figure 5 pump curves *E* and *F* are shown as unaffected by the change in viscosity. Actually the pumps will deliver somewhat lower pressure as the viscosity increases, reduction in pump pressure being possibly one per cent for the viscosity range applying in this particular case. This narrows correspondingly the range of operating pressures.

Depending upon individual case investigation, it may prove advantageous to install an adjustable-speed supplementary pump to handle full station throughput and deliver an adjustable incremental pressure to maintain the total pressure at the value determined by maximum viscosity, thereby realizing at lower viscosities greater flow than would be otherwise obtained. However, the benefit from this procedure depends upon the slope of the pump characteristic and the viscosity range. In the case of this line, examination of typical pump characteristic *F* in conjunction with the line curves indicates that this procedure would increase the flow by only about one per cent (*c*) for the average viscosity of 70 SUS and by about two per cent (*d*) for the minimum viscosity of 52 SUS. These increases are approximately two and five per cent, respectively, (*e* and *f*) for pump characteristic *G* which has an unusually high slope in the normal operating range.

The conditions here represented are those of a low-viscosity crude, operating in the turbulent flow range. For highly viscous crudes these differences become many times greater and the pipe line flow may change from turbulent to laminar, requiring quite different treatment. Actually the pipe line represented by Figure 5 is being equipped with constant-speed pumps of relatively flat characteristic, three full-head and one half-head unit per station, each station including an automatically controlled throttling valve to limit the station pressures.

Pump Speed Control to Accommodate Variations in Specific Gravity and Viscosity of Line Content

A much greater benefit from adjustable pump speed can be obtained where the line handles liquids of widely varying specific gravity and viscosity. This is usually the case for refined products lines, which are called upon to transport liquids commonly ranging from gasoline to Diesel fuel. The heavier products also generally have higher viscosity, both properties increasing the pressure required for a given flow in the pipe line. Figure 6 shows characteristics for a certain line, based on measured flow rates and pressures, for gasoline having specific gravity 0.74 and kinematic viscosity 0.79 centistokes, and for Diesel fuel having specific gravity 0.855 and viscosity 6.48 centistokes, at 40 degrees Fahrenheit line temperature. At higher temperatures the variation in line characteristics will be less for a given range of products. For combinations of product range and temperature range the variation may be greater.

In Figure 6 the pressure for 100 per cent flow of 0.74 specific gravity gasoline is taken as 100 per cent. Consider that the working pressure delivered by each station is to be limited to 105 per cent for reasons of pipe stress. Consider also that each station has three pumps in units of 1+1+1/2 head, as before, with $2^1/_2$ units in service for 100 per cent flow of gasoline. Then, when the line is carrying diesel fuel the pump pressure with $2^1/_2$ units exceeds the allowable working pressure. Reduction of the station discharge pressure to the established limit may be accomplished by throttling through pressure drop *p1p2*, or by reduction of pump speed. In the case here shown an estimated saving of approximately 12 per cent of the power required for full-speed operation can be effected by reducing the speed of the half-head unit to approximately 52 per cent of its full speed, using either a wound-rotor induction motor or a squirrel-cage motor with hydraulic coupling.

For this application the hydraulic coupling drive, compared to the wound-rotor motor, has the advantage of using a constant-speed squirrel-cage motor which is the simplest type, generally available in explosion-proof construction and requiring the simplest control.

In the case just proposed, with $2^1/_2$ unit operation there could be some further speed reduction of the half unit in order to

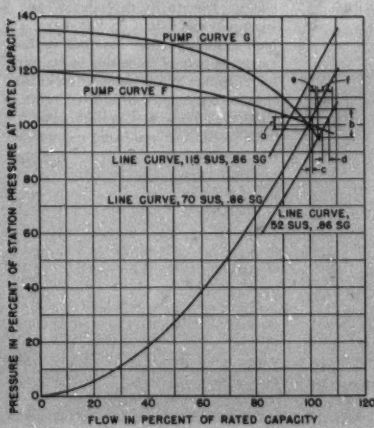


Figure 5 (left). Variations in station operating pressure for typical viscosity changes in crude oil carried by line

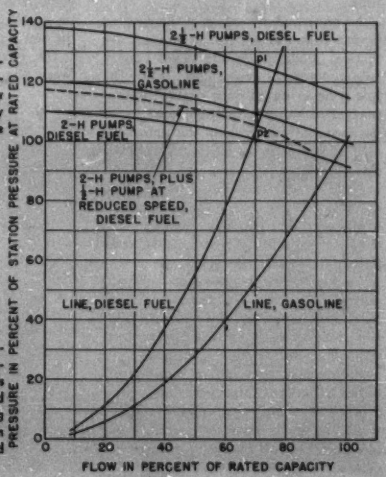


Figure 6. Application of variable-speed pump in series with constant speed pumps, for handling products varying in specific gravity and viscosity

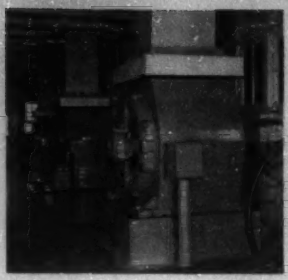


Figure 7 (left). 1,500 - horsepower open-type squirrel-cage induction motors driving crude oil pumps through fire wall

Pumps are shown in Figure 11

Figure 8. Centrifugal-pump drive by explosionproof motors eliminates fire wall and simplifies station layout

reduce the maximum pressure on the line. However, at some point the performance would come so close to the 2-unit operation that it could not be justified.

If the conditions of operation impose a wider range of line characteristics than shown in Figure 6, the situation can be met by installing all pumps the same in size. Variable speed drive on one pump then provides pressure control on each step of station capacity, with sufficient range to cover the intervals between successive steps.

Due to slip in the hydraulic coupling it is necessary to size the impeller of the adjustable-speed pump to a maximum speed of about $2^1/_2$ per cent lower than the motor speed.

The variable-speed pump should be the final unit in the series, because at fractional speeds it becomes a pump of smaller capacity that should be fed from, rather than feed to, an adjoining full-capacity unit.

An important advantage of this method derives from the fact that in practice, the stations along a line are never exactly balanced even at design flow. Since the line is never on an even grade, the proportion of static to friction head varies between stations, making the unbalance still greater at flows other than the design rate.

Under these conditions it is very desirable to make minor adjustments of the station discharge pressure and this can be done by speed control over a narrow range, at very small loss.

Employing speed control on only one pump per station, the proposed system leads to a simpler and less costly installation than required for speed control on all pumps.

It is evident that each application requires individual analysis, taking into account the range of conditions over which the line is intended to operate and also the range of loading of the pump drives.

Motor Construction

Not only must the motor's operating characteristics be considered, but also the suitability of its construction for the hazard which may exist at the motor location, due to hydrocarbon vapors in the atmosphere.

Many early installations on crude lines employed open motors. Today's preferred practice is to use open motors only if installed in a room separated from the pump room by a tight partition having a seal through which the motor shaft passes to the pump room. Figure 7 shows the motor room in such a station. To prevent recirculation of the exhaust air from the motor room, a duct from the top of each motor frame carries the air stream out through the roof. In a layout of this type it is desirable to keep the motor room under pressure of air drawn from the outside of the building away from the pump room. This is particularly true if the motor-control apparatus is located in the motor room.

The inconvenience of a partition between the pumps and motors and the problem of motor room ventilation are eliminated by installing the motors in the pump room, and the switchgear in a separate nonhazardous location. This requires that the motors be constructed safe for use in hazardous atmosphere. The explosionproof motor is designed for this purpose, and in the squirrel-cage induction type is available in ratings up to 1,250 horsepower at 3,600 rpm. The simplicity and convenience of layout, and the high degree of safety obtained, make this motor the preferred type in modern practice. Figure 8 shows an installation in a products line station, one of 30 stations using motors of this type in ratings up to 900 horsepower.

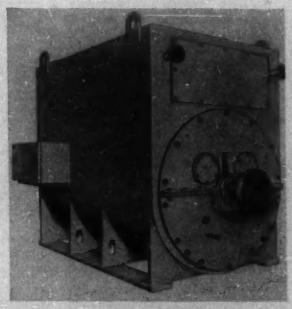


Figure 9. 2,250-horsepower squirrel-cage motor of inert gas-filled type for operation in hazardous location

Motors beyond the range of explosionproof construction are available in the inert gas-filled type. This form has been built in ratings as large as 2,250 horsepower at 1,800 rpm and is available in even larger sizes. The motor shown in Figure 9 is enclosed in a housing pressurized with inert gas which is recirculated through the windings and a built-in water cooler. Due to its dependence upon a gas supply and a water cooling system, the inert gas-filled motor has thus far been employed only in processing plants, pipeline engineers having preferred other solutions better adapted to isolated locations which are often limited in water supply.

Enclosed, duct-ventilated motors have been used both in refineries and pipe lines, particularly for ratings larger than obtainable explosionproof. In this type of motor, safe air from a location outside of the hazardous area is brought through a duct to the ends of the motor housing and, passing through the motor, leaves the frame at top or bottom through a discharge duct to a remote termination. Such an installation is shown in Figure 10.

A large crude line now under design will employ in each station three 1,250-horsepower and one 600-horsepower motors of this type, 1,800 rpm. In this installation the duct system of each motor

will be pressurized by a separate blower at the air intake, to put the entire system including the motor housing under pressure, causing any leakage to be outward from the duct system, so long as the blower is operating. The duct system will be purged by timed operation of the blower before the motor can be started. The automatic control includes an air-flow relay at the termination of the exhaust duct, arranged to stop the motor upon failure of air flow.

Outdoor submerged vertical barrel-type pumps have been employed extensively for moving products from storage tanks in terminals and for suction-booster service from tankage to pipe-line stations. Some of these are of the stuffing boxless type, using an oil-filled squirrel-cage induction motor submerged in the pump barrel. Recently, installations of the latter type have been made for main line service on small pipe lines.[5]

Motor Starting Equipment

The advantages of the squirrel-cage induction motor include that of requiring the simplest form of starting equipment. Ultimate simplicity is attained when the motors are started on full-line voltage, a possibility that should always be fully explored when the power service is being arranged.

Centrifugal pumps in pipe-line service are ordinarily started with discharge valve closed, to minimize the torque requirement during acceleration. Motors for this service are accordingly designed with low-starting and accelerating torque, in order to obtain low-starting current with good efficiency. As a result the voltage at the motor terminals usually must be not less than approximately 75 per cent of normal, in order to provide a reasonable margin of torque for acceleration. This means that if autotransformer starting is used, the lowest standard tap voltage practical is that of the 80 per cent tap.

Starting conditions are improved if the station capacity is divided into two or more units, an arrangement desirable for flexibility, as already shown.

Motors should be started on full voltage where the drop in station-bus voltage will not impair the operation of other equipment at the station, or where the smaller voltage drop on the high-voltage feeder serving the plant is not objectionable from the standpoint of service to other power consumers on the system. As an indication of the extent to which full-voltage starting has been practiced, this method was used in about half of the stations on the war emergency "Big Inch" line employing three 1,500-horsepower motors per station.

A number of cases have been encountered where power service to a remote station location was available only over a very weak circuit or on a heavily-loaded portion of an existing power system. Here the capacitor has been used to advantage as a means of reducing the lagging component of motor current.[6]

If corrective effect is needed only while a motor is being started, an intermittent capacitor may be employed with control arranged for energizing the capacitor only during those intervals. In one installation of this type, a capacitor capable of delivering 2,000 kilovars at normal station voltage enables a weak feeder to serve a products line station containing two 900-horsepower pump drives.

Where the power rate schedule justifies improvement of running power factor, continuous capacitors are employed, frequently subdivided so that a section may be switched simultaneously with each motor. Such applications have been made in sections as large as 1,400 kva. Three capacitor sections of this rating have been so applied with three 2,000-horsepower motors in a gas-line compressor station on a power supply of limited capacity.

Location and Arrangement of Motor Control

It is common practice to install the motor control remote from the pump room or in a separate room maintained nonhazardous by pressurized ventilation. With this arrangement the control is, in general, of conventional type which is advantageous from the standpoint of convenient maintenance.

Starting equipment for the main pump motors, which operate usually at 2,300 or 4,160 volts, may consist of air or oil circuit breakers, or of contactors in combination with current-limiting fuses. In most main line stations the use of circuit breakers in metal-clad switchgear is justified. Required interrupting capacity is frequently within the 50,000-kva rating of the light-duty class of switchgear, and even in the largest existing stations 100,000-kva interrupting capacity is adequate. Among the advantages of metal-clad switchgear for pipe-line service are ease of installation, safety to personnel, and minimum down-time for maintenance, particularly when air circuit breakers are used.

The auxiliary drives in a pipe line pumping station consist of such items as motorized valves, suction booster pumps, sump pumps, meter-calibration pumps, pump room exhausters, ventilating fans, and compressors for instrumentation air supply. Most of these applications require explosionproof motors. Usually

Figure 10 (below). Duct-ventilated motors in a pipe-line pumping station

Figure 11 (right). A crude-line pump installation with manually-operated suction and discharge valves

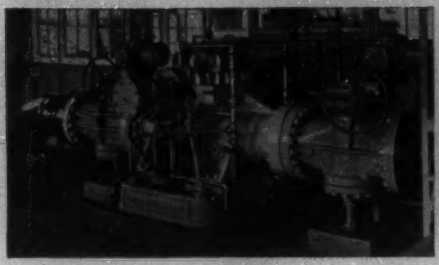


Figure 12. A refined-products line pump with motorized suction and discharge valves, all remotely controlled as a unit

Figure 13. Centralized control desk in initial station of a modern products line

Gauge panel, sealed into pump room window, mounts flow recorder, recording pressure controllers, pressure gauges, and temperature recorders actuating temperature protective relays. Console at center mounts pushbuttons and indicating lights in simplified diagram of station piping

the starters are banked in control centers of conventional form in the switchgear room.

A power-distribution layout frequently followed feeds the station bus through a main breaker installed in the transformer substation at a distance of 100 feet or more from the station. Tripping the main breaker clears all circuits on its load side, and for this purpose explosionproof control devices are provided inside the station.

To avoid interrupting essential loads, such as station lighting and control room ventilation, these are served through a separate auxiliary transformer on the supply side of the main circuit breaker. Explosionproof equipment is installed throughout the station on this essential auxiliary circuit. A pressure-actuated relay may be installed on this circuit to sound an alarm and light a warning light upon failure of ventilating air delivery to the control room.

For interchangeability with a common spare circuit breaker, the main circuit breaker may be of the same type as the motor-starting circuit breakers and installed in outdoor metal-clad switchgear. This switchgear may also house the auxiliary transformers and low-voltage circuit breakers for auxiliary power distribution.

Centralized Station Control

One of the prime advantages of electric power for pipe-line operation is that of centralized control to simplify the duties of the station attendant. When its possibilities are fully exploited, this includes the grouped location of pushbuttons for starting and stopping the main pumps, and the automatic sequence operation of motorized suction and discharge valves associated with each pump, also indicating light supervision of the position of principal valves both motorized and hand operated, and light supervision of critical auxiliaries. It also includes alarm and identification in case of functioning of various protective devices. The control station is sometimes built explosionproof and located in the pump room, but is more commonly installed in the switchgear room with a sealed window to afford view of the pump room.

To illustrate one advantage of centralized control, Figure 11 shows a pump installation using manually-operated suction and discharge valves each requiring over 100 turns of the handwheel for complete travel. Figure 12 shows motorized pump valves controlled in sequence with the pump motor and requiring only a single start-stop pushbutton station for the combined unit. The conventional all-steel control desk of a few years ago has been improved by use of a clear plastic sheet for the top surface. This provides greater durability and permits a simplified piping diagram to be applied to the under surface of the plastic, with clear areas at appropriate points acting as light indicator lenses. Using flushtrimmed pushbuttons this desk has no devices projecting from the top surface, minimizing the possibility of false operations.

Figure 13 shows the control desk console for the initial station of a refined products line completed in 1948. This desk provides control and supervision of three main pump units with motorized valves. It includes also indicating light supervision for two motorized manifold header valves, and a number of manually-operated valves in the system incidental to the storage, batching and metering of the several types of products transported through the line. Each storage tank has a booster pump for delivery of its product to the station suction. These tanks are represented on the station control desk, each with a start-stop pushbutton and supervising light. Equipment of this type permits the exacting procedures of products-line operation to be carried out effectively with minimum personnel.

The advantages of centralized control are being realized in modern crude line installations as well. A crude line project now under design will employ six stations, five of which will serve two parallel but separately-operated lines. In each station a control desk will centralize the operations for both lines.

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Discussion

W. G. Taylor (General Electric Company, Schenectady, N. Y.): The information in Hyde's paper is a good example of the type

of electrical engineering so necessary in industrial application engineering work. A good electrical application engineer must also be a good mechanical engineer. His success often is measured by the skill with which he uses simple and often standard, or nearly standard, electric equipment to accomplish results which to the layman might seem to require much more complicated design.

In the case of the oil pipe line, the problems of operation are complex, as Hyde very clearly indicates, and the electrical engineer must not only work closely with the pipeline designer, but also have clear and complete knowledge of what is to be accomplished, to be able to recommend a satisfactorily working arrangement of electric drive which will involve minimum investment and operating attention as well as low maintenance expense.

It is a simple matter to start a pipe-line centrifugal pump; but I would like to emphasize the fact that there may be difficulty in accelerating it up to full speed if the engineer does not compare the required pump torque with the available motor torque, and make sure that the starting equipment provides sufficiently high acceleration voltage for the purpose. This problem is tied in closely with the character of the power supply. If the power system is stiff, full-voltage starting of the pump motors can usually be permitted without difficulty. If it is weak, the voltage drop may prevent the use of a reduced-voltage starter, because the resulting motor accelerating torque would be too low to meet the requirements. It is obvious, therefore, that the use of reduced-voltage rather than full-voltage starting equipment is questionable in either case.

Hyde's paper is an interesting treatment of pipe-line pumping problems from a fresh point of view. The basic information which he gives might well be put in more complete tabular form, showing the relation of variable flow, viscosity, specific gravity, and resulting station pressures to the number of pumping stations, the number and size of operating units in each station, and the relative horsepower loads. His explanation of the method of operating multiple-unit pumping stations to give quite a number of available station-discharge pressures, when using only constant-speed motors to drive the pumps, shows the versatility of electric drive without elaborate design or complicated control.

The design of such stations is fundamentally the problem of the designer of the pipe line, but the electrical application engineer plays a large part in making the project a success.

W. H. Stueve (Oklahoma Gas and Electric Company, Oklahoma City, Okla.): The opportunity to review this paper with an idea of offering a discussion is appreciated because my association with the author covers the period of time during which practically every electrified pipe line was installed in the United States.

The author mentions the great increase in pipe line transportation during the war years of 1941 to 1945, and places emphasis on the accomplishments of the Big Inch pipe systems in that period, but he overlooked stating that prior to 1941 the pipe line transportation industry had installed almost 200,000 horsepower of electric motors on their main trunk lines, and that after the 20" and 24" "Big Inch" lines were completed, the total amount of electric prime movers in use totalled 450,000 horsepower. Today, this means that almost one-half of all prime movers required by the oil and oil-product pipe lines are electric motor driven. The sales rate of equipment for this type of service is now about 3 horsepower of electric motors to 1 horsepower of internal combustion engines. The greatest impetus given to convince the oil pipe line builders of the satisfactory and economic performance of electric motors, was the complete electrification of the "Big Inch" system, which contained 26 4,500 horsepower stations and 29 3,750 horsepower stations on the 24-inch crude line and 20-inch oil-products line respectively.

Generally speaking, the part played by the electrical industry in making this capacity of some 200,000 kw available from Texas to New Jersey is not realized by the public, but the private utilities delivered $2^1/_4$ billion kilowatt-hours from January 1943 to August 1945 to the "Big Inch" system, with shutdowns due to power failures of only 15 hours out of 22,870 hours operated, less than $^1/_{10}$ of 1 per cent of total elapsed time. The foregoing could be added to the author's Advantages of Electric Power.

The author under the caption Motor Applications only vaguely hints that electric motors can be used for natural gas transmission and also omits to state the reason why motors are being used in this type of service.

It is true that 84 1,250 horsepower, 3,600 rpm motors, or 105,000 horsepower, originally installed on the 20-inch products line are being installed for direct drive of centrifugal compressors on the "Big Inch" lines to pump natural gas.

The author's paper is supposed to cover only the petroleum pipe lines electrified, but since the natural gas industry is so closely allied to the oil industry, and further, since the use of electric motors for gas transmission opens up an entirely new field for power usage, it was felt that some discussion of the reasons why motors were being considered for gas pumping would be appropriate at this time.

In the past the conventional installation consisted of gas engine driven reciprocating compressors which cost $200 per horsepower and the station spacing (80 to 100 miles) was such that the compression ratio was of the order of 1.8 to 2.0. When electrical centrifugal compressors are used the station spacing can be one-half (40 to 50 miles) resulting in compression ratios of about 1.3 and savings in power equipment of about 30 per cent in total power required. The electric driven centrifugal compressor can be installed for about $70 per horsepower and this results in a ratio of first costs of four for the gas engine reciprocating compressor in-

Figure 1. Typical capacity curves of gas transmission lines

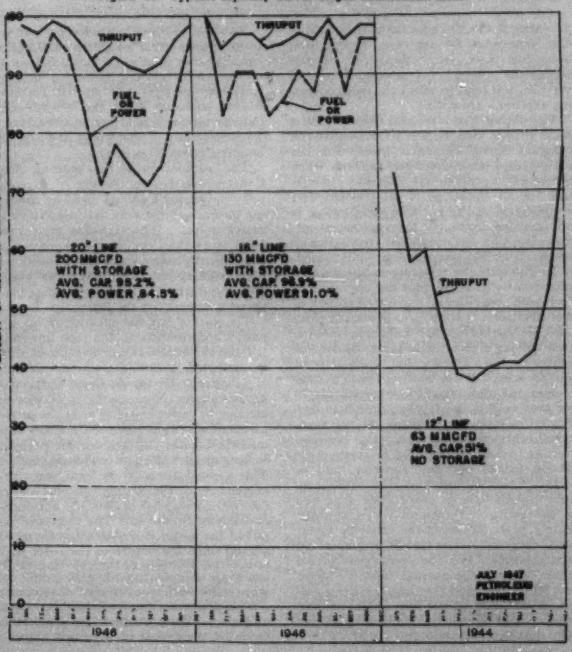
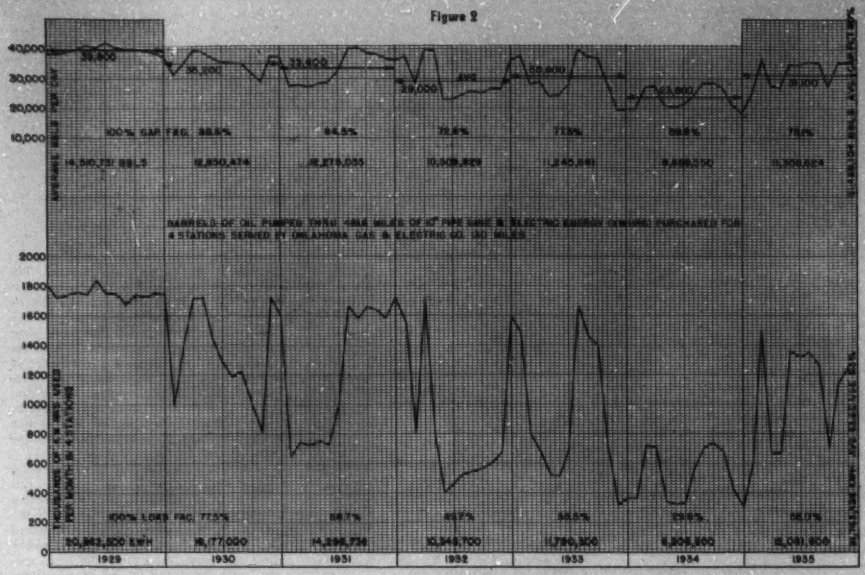


Figure 2



stallation, to one for the electric centrifugal compressor installation. Another factor which favors the electric centrifugal installation is that it consumes no fuel and can deliver at destination practically the same volume of gas as introduced at the origin of the line, and further this gas not consumed enroute is worth the ultimate delivered sales value. This matter is important when one realizes that any given gas engine station will generally consume as much gas in a year for fuel as is its daily capacity. Thus in a long pipe line equipped with gas engines consisting of 20 stations, the annual consumption of fuel would be 20/365, which in the case of the "Big Inch" system capable of delivering 450 to 500 million cubic feet per day, would amount to a loss in annual revenue of almost $2,500,000. In making a comparative operating cost study, this loss in ultimate revenue would appear as a credit to the electric operation cost. Probably the most important factor however which influences the use of electric power in natural gas transmission, is that pipe lines never operate at 100 per cent capacity day after day throughout the year. As shown in Figure 1 of the discussion, when this decrease in capacity factor occurs, the energy required for pumping falls off rapidly, and the energy required expressed in per cent of maximum is equal to the capacity factor expressed as per cent of maximum design capacity raised to approximately the third power.

Since fuel costs represent only 15 per cent of the total operating costs of a gas engine station, any change in fuel usage due to lower capacity factors of the line, will not materially affect the total operating cost. On the other hand in the case of electric driven equipment, where the electric energy cost represents 80 per cent of the total operating costs, any change in electric energy use due to lower capacity factors of the line, will tend to affect the total operating expense a great deal.

The author has developed theoretically a great deal of information to show that the constant speed induction motor for the main pumps can be used satisfactorily when decreased capacities are desired. This is true for all situations of oil gravities and viscosities, and the variable speed motor is not considered essential for economic operation except for the occasional installation of the small suction booster pumps mentioned.

To illustrate the economy in energy consumption by a long oil pipe line operating at decreased capacities is shown by Figure 2 of the discussion. Each pump station contains 2 400 horsepower induction motors, each driving a centrifugal pump capable of delivering full capacity and one-half total line pressure. If one motor in each station is operated, the capacity obtainable is 70 per cent, and if every other station is shut down the capacity is about 60 per cent. Many other combinations are also possible, such as one motor operated in every other station for 40 per cent capacity, and so forth. From Figure 2 of the discussion, note that the energy usage when expressed in per cent of maximum varies approximately as the square of the capacity factor when expressed in per cent of maximum design capacity. For the seven year period from Figure 2, the average capacity factor was 80 per cent and the electric energy consumed was only 63 per cent of the amount which would have been required at 100 per cent capacity operation.

As explained previously for gas pumping, oil pipe lines never operate at 100 per cent capacity over a period of time, and the economic use of electric power at decreased capacities permits the electric station to readily compete with the Diesel engine driven station, which fact accounts for the widespread use of motors for the pipe lines handling oil or oil products.

The author under the heading Motor Construction covers the use of explosion-proof motors, and savings made by eliminating the fire wall between the pump room and motor room. The explosion proof motor costs more than the open-type motor and consequently many stations are constructed with ordinary induction motors by venting the air ducts to outdoor openings and thereby eliminating the fire wall.

The author has not mentioned his company's contribution to the pipe line industry, but the double rating horsepower motor is worthy of particular mention. It generally happens that pipe lines, particularly products lines, have use for a 50 per cent increase in power capacity after the original installation is made, because midway intermediate booster stations become necessary to keep up with the demand for oil products. This necessitates a 50 per cent increase of horsepower at the original station, and the 600/900 horsepower or 300/450 horsepower motor was developed. The increased output of the motor was obtainable by merely adding some fan blades to the rotor to increase the ventilation, thereby maintaining the same temperature on the motor coils when more work was required of the motor.

This discussion is not offered in any sense as being critical of the fine presentation made by the author of the many contribu-

tions the electric motor manufacturers have made to the pipe line industry. While this discussion may seem to stray somewhat from the original text of the paper, it was felt that the matters of relation between capacity factor and electric power usage should be presented, because they are the most important matters from an economic viewpoint, which govern the choice of motors over Diesel engines in this industry. Surely some of the readers would wonder why electric power would be used when consideration is given to the wide spread between electric energy costs and the cost of fuel to produce the equivalent energy.

Oil Production by Electric Power

W. G. TAYLOR
NONMEMBER AIEE

IT is well known in the oil industry that no two oil wells are alike. However, their operation in producing oil falls into certain definite patterns which simplify the design, selection, and use of mechanical and electric power equipment necessary to maintain the production throughout the useful life of each well. It is the purpose of this paper to cover the specific conditions affecting the application of electric power to the operation of producing oil wells, the selection of electric equipment suitable for this purpose and the layout of electric power distribution systems in the oil fields. The magnitude of the production branch of the petroleum industry in the United States is indicated by the statistics in Table I. This table also shows the extent to which electrification has progressed and the estimated potential electric power market of the future.

Ten Methods of Producing Oil

Although oil wells drilled in a new field may flow naturally for a few weeks, months or even years, due to underground gas or water pressure, this pressure will eventually be dissipated to a point where oil flow will cease. Some artificial method of producing the oil must then be adopted. In the older oil fields the underground reservoir pressure may already be largely exhausted, and new wells drilled in such fields require artificial production methods immediately. There are thousands of such wells drilled every year. Most of the methods used to produce oil by application of power are included in the following list. Electric drive is applicable to all of them.

1. *Beam pumping of individual wells.* About 90 per cent of pumped wells are producing by this method, which employs a power-driven pumping unit incorporating a walking-beam to actuate a deep-well reciprocating pump by means of sucker rods.

2. *Central-power pumping of wells in groups.* In this method, a central power-driven unit actuates pull rods extending to the wells to operate the deep-well pumps. This unit is usually called a power.

3. *Gas lift.* Natural gas, or sometimes air, delivered by power-driven compressors, blows the oil out of the well. The operation may be continuous or intermittent.

4. *Centrifugal bottom-hole pumping.* This is exclusively an electrical application, using a submersible motor-driven centrifugal pump unit lowered into the well. This method sometimes has been combined with gas lift, to pump oil part way up the hole and then blow it out the rest of the way.

5. *Hydraulic pumping.* The deep-well reciprocating pump is actuated by oil under pressure, which is delivered down the hole by a power-driven pump at the surface.

6. *Plunger lift.* A free-traveling plunger actuated by gas pressure lifts the oil by moving the entire length of the tubing from the foot-piece to the flow head at the surface. The gas is delivered from power-driven compressors.

7. *Pneumatic rod lift.* The rods connected to the deep-well pump are raised and lowered by an air-actuated piston in a cylinder at the surface. Power-driven air compressors supply the air.

8. *Casing rod pumping.* In this method, the reciprocating deep-well pump, fixed in the casing at the bottom of the well and actuated by sucker rods from a pumping unit at the surface, delivers the oil up through the casing without the use of tubing.

9. *Swabbing.* The oil is produced by a swab which is raised and lowered in the well casing by a power-driven hoist. This method is employed for production only temporarily or in special cases.

10. *Repressuring.* This is an indirect method of production by prolonging the natural flow of wells. Natural gas is forced into the oil-bearing formation by power-driven compressors, to maintain sufficient underground reservoir pressure for the wells to continue flowing.

Table II shows the approximate limitations of each of these methods of production from the standpoint of depth of well and amount of production, and the approximate range of horsepower requirements. The application of electric drive to methods 1 to 5, inclusive, will be discussed in this paper, since they are employed in many oil fields. The other methods, except number 10, are not extensively used and will not be considered further. Some additional comments will be given on repressuring.

Control of Production from Flowing Wells

By means of automatic electric control, the flow of each individual well in a group of flowing wells on a property can be accurately timed in accordance with proration or other requirements. The flowing period each day can be scheduled progressively, simultaneously, or with any desired overlapping. The lease tanks can be filled consecutively or in the order of their emptying. When all tanks are filled, the flow stops automatically until a tank is again available. When a day's production quota is met, the oil flow is stopped until the next day, when it is resumed automatically on the same schedule. Such operation is accomplished by a time switch on each well, controlling electrically operated and interlocked valves. A master timer starts and stops the daily program, and temporarily stops all scheduled operations if an emergency arises. All operations can be recorded automatically.

Beam Pumping

Beam pumping uses a single-acting reciprocating pump located in the well. In

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W. G. Taylor is with the chemical and petroleum division of the General Electric Company, Schenectady, N. Y.

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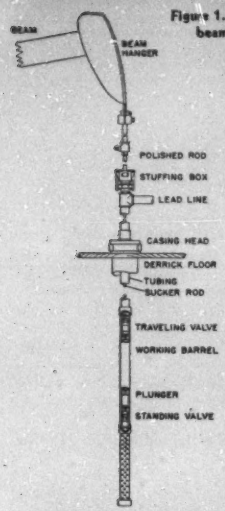


Figure 1. Oil-well beam pump

its simplest form, Figure 1, it consists of a working barrel with a valved plunger. The working barrel is attached to the lower end of a string of tubing. The plunger is suspended from a string of sucker rods which is hung from the walking beam and extends down inside the tubing far enough to keep the plunger inside the working barrel. The motion of the walking beam provides the reciprocating motion for the pump plunger. On the down stroke the lower valve closes and the upper valve opens to pass oil. On the up stroke the lower valve opens to admit oil and the upper valve closes to enable the plunger to lift the column of oil in the tubing.

The walking beam is actuated by a pitman connected to a crank on a gear reduction unit. The latter is usually connected by a V-belt drive to the pumping motor (Figure 2).

The oil lifted through the tubing by the pump is discharged through the lead line shown in Figure 1.

Pumping speeds vary widely, but generally range from 15 to 20 strokes per minute. A pumping speed under 10 strokes per minute is usually called slow motion pumping. High speeds up to about 35 strokes per minute are used in some cases to obtain a profitable amount of oil when the well produces a high percentage of water.

Normal practice is to confine the work of the motor to pumping and to employ a portable hoist to pull the rods and tubing when it is necessary to make repairs, replace pump parts, or clean out the well.

Method of Estimating Beam-Pumping Horsepower Requirements

Formulas which follow can be used for estimating horsepower requirements if due regard is given to the fact that some indeterminate factors may affect materially the power required. If, for example, the volumetric efficiency of the pump differs much from that assumed, or if a well produces considerable gas or sand, the calculations are likely to be quite inaccurate for the current operating conditions. Judgment and experience must be depended on to supplement the calculations and to interpret them in terms of probable future conditions.

Barrels per day =

$$\frac{3.1416 \times R^2 \times L \times S \times (\text{hrs pumped daily}) \times 60 \times 0.75}{231 \times 42}$$

Where

R = radius of pump plunger in inches
L = length of stroke in inches
S = strokes per minute
60 = minutes per hour
0.75 = assumed volumetric efficiency of pump
231 = cubic inches per gallon
42 = gallons per barrel

Hydraulic horsepower =

$$\frac{(\text{barrels per day}) \times 42 \times 8.33 \times (\text{depth in feet})}{(\text{hours pumped daily}) \times 60 \times 33{,}000}$$

Where

42 = gallons per barrels
8.33 = pounds per gallon of water (can substitute weight of oil if known, but allowance must be made for probable water content)
60 = minutes per hour
33,000 = foot-pounds per minute per horsepower

Friction horsepower at polished rod =

$$\frac{(\text{weight of rods in pounds}) \times (\text{stroke in inches}) \times (\text{strokes per minute})}{1{,}600{,}000}$$

Where

1,600,000 = empirical factor determined from many tests

Motor horsepower rating =

$$\frac{(\text{hydraulic horsepower}) + (\text{friction horsepower at polished rod})}{0.93 \times 0.7}$$

Where

0.93 = assumed mechanical efficiency of pumping unit
0.7 = motor heating factor on pumping duty cycle

Figure 2. Typical oil-well beam pumping unit

In explanation of the motor heating factor of 0.7 in the last formula, it will be seen that the calculated hydraulic horsepower is based on average daily production and therefore is the average horsepower. The fluctuation of current on the pumping stroke is of such character that the heating effect or rms of the load, which determines the motor rating, is approximately (average horsepower)/0.7.

Types of Motors Used for Beam-Pumping and Some Application Factors Involved

The 3-phase squirrel-cage induction motor is almost universally used for beam-well pumping. This motor is available in three types, which for brevity will be designated by letters. They are the normal-torque or *NT* motor, the high-starting torque or *HT* motor, and the high-slip or *HS* motor which also has high starting torque. For this service only motors rated on a continuous basis should be used.

Some or all of the following factors are involved in the selection of the most suitable type of motor.

1. A rather high starting torque is required for a pumping unit. In cold weather the high viscosity of the grease or oil in the bearings and gear unit demands an especially high starting torque which may be out of proportion to the motor size. Therefore, small motors need a relatively higher starting torque in percentage of full load torque than do large motors. The actual foot-pounds starting torque required, over a range of motor sizes used on pumping units, will not vary nearly as widely as the motor sizes. Usually there is not much advance information as to how great the required starting torque will be. It is therefore customary to apply a motor with a high starting torque (approximately 250 per cent of full load torque), such as the *HT* or *HS*. when the rating is under 10 horsepower,

For motors of 10 horsepower and larger, it is reasonably safe to apply the normal starting-torque motor (approximately 150 per cent of full load torque), unless other reasons dictate the use of the *HT* or *HS*. On some specific applications it has been found advisable to use the *HT* or *HS* for 10, 15, or 20 horsepower sizes to insure sufficient starting torque. Such decisions must be based on local operating conditions. For example, *HT* or *HS* motors are often advisable for wells with paraffin troubles.

2. On the normal duty cycle of a beam-pumping unit, there are two power peaks on each pumping stroke, one when the rods and oil are lifted, and the other when the counterweight is lifted. When the electric power supply is generated in a small isolated power plant, these recurring peaks of a group of wells will periodically coincide, as illustrated in Figure 3, and result in unsatisfactory voltage regulation. Under these circumstances, and regardless of all other considerations, high-slip motors (having 8 to 13 per cent slip instead of a normal of approximately three per cent), should be used to reduce the current peaks (Figure 4). If the power is purchased, or if the isolated power plant is large, either *NT* or *HT* motors can be used, provided other conditions are favorable.

3. By slowing down as the peak load comes on, the high-slip motor allows energy stored in the rotating parts of the rig and motor to help carry the peak. Some operators desire this type of motor for the cushioning on the power system resulting from this speed variation. However, cushioning of mechanical shocks in this manner will be obtained only if the average strokes per minute are allowed to drop correspondingly, and even then the effect will be small. As this would probably decrease the production from the well and possibly give a speed out of resonance with the sucker rods, normal practice is to use different V-belt drive ratios for high-slip and normal-slip motors, so as to obtain the same pumping speed in either case. Under these circumstances there is no material difference in the mechanical shock loads and the only cushioning effect is that obtained in the electric input to the motor.

4. High pumping speeds, of approximately 30 to 35 strokes per minute, are used in many cases, such as wells producing a very high percentage of water. Such speeds result in high motor-input peaks even when the pumping units are properly counterbalanced. High-slip motors give the best results in such cases, as they reduce the current peaks and minimize the motor heating.

Although the efficiency of the high-slip motor is lower than that of the normal-slip motor, this does not necessarily mean that the former will have a higher power consumption. The reduction in current peaks effected by high slip reduces the power losses in proportion to the square of the current, which may more than offset the effect of the lower motor efficiency. Even this is not necessarily the result in all cases, as it depends on the conditions at each well. The net result may be an increase or decrease in power input, but too small to be a factor in the selection of the size of motor.

Guide to Motor Selection for Beam Pumping

In line with the foregoing considerations, the following guide to motor selection is offered to cover the most important factors. There are many other conditions in oil-well-pumping service which can influence motor selection, especially in borderline cases. No attempt will be made to cover these, because they are usually obvious to engineers and specialists who give the applications careful consideration.

1. The normal-slip normal-starting-torque (*NT*) motor is usually applied when:

a. The motor size is 10 horsepower or more, and power is supplied from a large generating plant or from the power system of an electric utility and where the wells are properly counterbalanced and paraffin not excessive.

b. The evidence obtained from tests, or from comparison with other wells, shows conclusively that 150 per cent starting torque will be sufficient.

2. The normal-slip high-starting-torque (*HT*) motor is usually applied when:

a. The required motor is less than 10 horsepower, unless it is definitely known that an *NT* motor will be satisfactory.

b. Existing well conditions are not thoroughly known.

c. There is conclusive evidence that very high starting torque (up to 250 per cent of full-load torque) is required.

3. The high-slip high-starting-torque (*HS*) motor is usually applied when:

a. Power is supplied from a small isolated generating plant. This recommendation holds true regardless of all other considerations.

b. The user desires the additional cushioning effect which the high-slip motor provides, as compared with a normal-slip motor.

c. Under any conditions, it is advisable to reduce the momentary current peaks.

d. An unusually high pumping speed is required, resulting in very high peaks, for example, when the water/oil ratio is high.

Both high-slip and medium-slip motors are available. As the slip permits the stored energy in the rig to carry part of the peak load, and as this stored energy is small under any circumstances, the high-slip motor should be used so as to utilize the available stored energy as fully as possible.

Double- and Triple-Rated Motors for Beam Wells

The triple-rated single-speed squirrel-cage induction motor has three horsepower ratings in the ratio of 10-7-4, obtained by changes in stator connections, and all at the same speed; the double-rated motor is the same with connections for the intermediate rating omitted. The main advantage of the lower ratings is to improve the power factor on the lighter loads. A slightly better efficiency may also be gained. Where the power requirements of the various wells on a lease differ considerably, such a motor, applied in the same size to all the wells, will give maximum flexibility in the use of motors on light or heavy wells, while keeping the stock of spare motors and renewal parts down to a minimum.

In the initial stages of pumping after a

Table I. United States Oil-well Pumping Statistics at the End of 1946

Total number of producing wells	424,286
Number of free-flowing wells	48,387
Number of pumped wells	375,899
Percentage of wells pumped electrically (January 1946), approximately	21
Installed electric motor horsepower for oil production (January 1946)	772,000
Annual electric power consumption, kilowatt-hours	1,515,000,000
Oil production in 1946, barrels	1,731,889,000
Oil produced electrically in 1946, barrels	363,700,000
Average electric power consumption, kilowatt-hours per barrel	4.2
Estimated potential electric power market for wells pumped by other than electric drive, kilowatt-hours per year	5,746,000,000
Number of wells put on the pump in 1946	14,441
Estimated potential electric power market for wells put on the pump annually, kilowatt-hours per year	277,267,000

Table II. Oil Production Methods

Producing Method	Depth of Producing Well in Feet Minimum	Depth of Producing Well in Feet Maximum	Fluid Required to be Pumped from Each Well, Barrels per Day Minimum	Fluid Required to be Pumped from Each Well, Barrels per Day Maximum	Horsepower of Prime Mover Required per Well Minimum	Horsepower of Prime Mover Required per Well Maximum
1—Beam well pumping	800	8,000	1	500	3	75
2—Central-power pumping	800	2,500	1	25	1	5
3—Gas lift	3,000	6,500	500	5,000	75	500
3A—Intermittent gas lift	4,000	6,500	100	1,000	25	100
4—Centrifugal bottom-hole pumping	1,500	6,500	1,000	3,000	25	200
4A—Combination of no. 4 and no. 3	5,000	6,500	3,000	5,000	150	200
5—Hydraulic pumping	5,000	8,000	50	300	10	40
6—Plunger lift	3,000	6,500	100	1,000	25	100
7—Pneumatic rod lift	3,000	6,500	50	500	25	100
8—Casing rod pumping	1,000	2,500	2,000	3,000	25	50
9—Swabbing	4,000	6,500	1,000	2,000	75	150
10—Repressuring	800	7,500	5	25	10	100

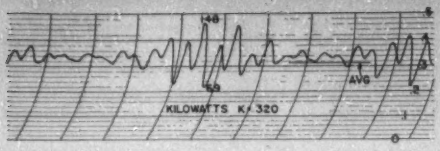


Figure 3. Load characteristic of a group of beam-pumped wells

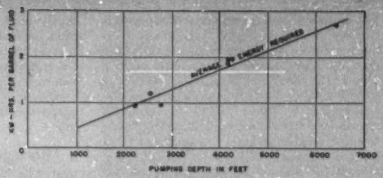


Figure 5. Power consumption of beam-pumped wells listed in Table III

well has ceased flowing, the lowest rating may be used, because the remaining reservoir pressure will still help to lift the oil. As this pressure declines, the intermediate rating and eventually the highest rating will be required. Then in time as production declines, the lower ratings may be used again. These changes of rating will also be advantageous to match changes in allowable production under proration rules. The highest rating may be used also for driving a friction-clutch rod and tubing hoist.

Recent trend has been away from this type of motor in favor of several sizes of single-rated motors, the practice being to change the motor size on a well to suit changing conditions. Generally a field does not require pumping to be started all at one time, but it begins at one edge and progresses across the field. The smaller sizes of motors are used at the front line, and larger motors progressively to the rear.

Motor Enclosure for Beam Wells

Well-pumping motors are usually installed outdoors. They are available with dripproof, splashproof, totally enclosed fan-cooled, and explosion-proof frame constructions. The present trend is toward the use of dripproof motors where splashproof have been used extensively in the past. Under severe weather conditions, the totally enclosed fan-cooled motor will give long service with less maintenance than either the dripproof or the splashproof motor. In a few places where heavy snow and sleet conditions prevail, dripproof motors enclosed in ventilated "doghouses" or sheds have been used. Explosion-proof motors are not required normally because the well is capped when the motor is operating, and the motor is shut down when the well is open.

Control for Beam-Pumping Motors

Control equipment is assembled in a pole-mounted, ventilated, outdoor case. It normally includes a fused line switch, a motor-starting magnetic switch, a control switch, undervoltage and temperature overload protection, and, when required, an automatic time switch and a lightning protector.

Time-cycle pumping is used where production is prorated, or where stripper wells are pumped only a few hours a day. The time switch starts and stops the motor automatically at preadjusted times each day, and may be arranged to stop the pump one or more days a week if desired. Exact production schedules are thus met on time without attention from the pumper. Stripper well production may be increased by proper timing. For example, in one case a well pumped by manual control once a day required 12 hours to fill to its maximum head, but filled to 70 per cent of this head in six hours. By means of time-switch electric-motor control, it was pumped twice a day at 6-hour intervals, and produced 140 per cent (2×70 per cent) of the oil previously produced, a gain of 40 per cent.

Power Consumption for Beam Pumping

As oil wells differ in their characteristics, and the operating conditions of each change from time to time, estimates of electric power consumption are not only difficult to make but are likely to be inaccurate. When wells cease flowing and go on the pump, the power increases progressively as the reservoir pressure declines, and then eventually decreases as production declines. The point in the productive cycle at which electric drive is applied will govern, to a large extent, the power consumption to be anticipated.

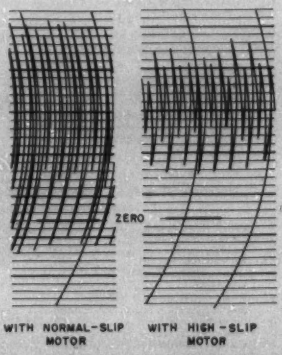


Figure 4. Effect of a high-slip motor in reducing current peaks on a beam-pumped well

Although the status of the production cycle was not available for the 207 wells listed in Table III, a considerable range of depth is covered, which makes it possible to plot the power consumption curve shown in Figure 5. This is a fairly representative curve, although for small producers increasingly higher average kilowatt-hours per barrel can be expected with increase in depth.

Effect of Counterbalancing Beam Wells

A walking-beam pumping unit is counterbalanced by weights mounted on the end of the beam or on the crank. By adjustment they offset the weight of the rods in the well plus about half of the weight of the oil lifted on each stroke.

The best possible counterbalancing makes the two power peaks equal (Figure 6). This can be checked readily by a portable ammeter.

Proper counterbalancing not only reduces the torque required to rotate the crank but also minimizes the power consumption because of the reduction in losses.

Beam-well Cost Considerations

It is practically impossible to prepare a general cost comparison between electric drive and other forms of power which will be of much value for consideration of specific applications. This is due to wide variation in operating costs as well as conditions. Furthermore, oil company records of performance are very often so incomplete, or pertinent costs are so frequently charged to and buried in general accounts, that it is difficult to determine facts essential for a comparison. Con-

sideration of costs will therefore be confined to some available data and a few comments.

A chart of relative installation costs of four types of beam-pumping equipment is shown in Figure 7. As the costs were determined in 1938, they will be far from correct now, but the comparison is of interest. The data were derived from reliable contractors and equipment manufacturers, and applies to mid-continent wells producing 300 to 400 barrels per day from a depth of approximately 4,000 feet and equipped with 40-horsepower engines or 25-horsepower motors. Another comparison, shown in Figure 8, is based on costs (1940–1941) in Illinois oil fields, covering electric motors and multicylinder gas engines.

In figuring operating expenses, some operators are inclined not to give due consideration to fixed charges, which are of course important factors. Before the war, interest was usually figured at six or seven per cent, but varied with the locality and the apparent risk involved. Depreciation of ten per cent per year is conservative. Taxes and insurance average about three per cent per year.

Operating costs include power, labor and maintenance. Gas used should be charged at its marketable value, if any. Labor cost will depend on the number of wells which can be attended by each pumper. Maintenance includes costs of repairs, renewal parts, reconditioning, and lubrication. In making comparisons between various forms of power, consideration should be given also to loss of production due to shut-downs and to the relative amount of rod breakage experienced. Shut-downs may not be important on part-time producers, but rod breakage can be so on any beam well. For example, on a West Virginia property, installation of electric drive resulted in the practical elimination of rod breakage on wells where breakages had previously averaged one a year per well. Under similar circumstances in Venezuela, rod failures which had averaged between three and four a year per well were reduced over 80 per cent after electrification.

Central-Power Pumping

In central-power pumping, the power from a single motor is transmitted mechanically to a group of wells through a pull-rod system. The motor drives a vertical shaft eccentric, and pull-rods are hooked to the eccentric strap. They extend horizontally to the several wells, where they actuate pumping jacks, similar to bell cranks. To these are attached the sucker rods in the wells. The motor drives the rig through either a bevel-gear drive (Figure 9) or a large vertical-shaft band-wheel with belt drive. The direction of pull of each well on the eccentric is so laid out, by mechanical means, that the wells counterbalance each other. When the wells are pumped intermittently, they are hooked to the "power" on a staggered schedule and the "power" runs continuously. Experience is the only practical rule for estimating the power requirements. Records of a large number of very shallow wells, 1,000 feet or less, show an average of 1.5 horsepower per well, and a minimum of less than 1 horsepower. Power consumption of several groups of wells is shown in Table IV.

A standard squirrel-cage or slip-ring induction motor with simple starting and protective equipment usually meets all requirements for central-power pumping.

Gas Lift

The gas-lift method of oil production is used occasionally. It employs compressed natural gas to blow the oil out of the well. Air may be used sometimes when there is insufficient gas. By the simple method illustrated in Figure 10, the gas is forced down through a pipe suspended in the hole, and blows the oil up through the tubing.

A group of 75- or 100-horsepower compressors, according to the oil company's individual requirements, is the usual type of installation. Standard squirrel-cage induction motors are used to drive the compressors.

Because all of the heat of compression of the gas is lost, this is a costly method of producing oil, but it has the advantage of being able to handle large volumes of fluid.

Centrifugal Bottom-Hole Pumping

A well with heavy potential production in excess of the capacity of deep-well reciprocating pumps can be pumped with a submersible centrifugal pumping unit. Such a unit consists of a cylindrical, multistage, centrifugal pump driven by a high-speed squirrel-cage induction motor of the same diameter, and directly coupled to the pump with an intervening sealing unit. The complete unit is suspended in the well by tubing, and receives electric power through a cable clipped to the tubing. The required motor capacity depends on the number of stages in the pump. Sizes available range from 52.5 horsepower in $4^1/_8$-inch outside diameter and 97.5 horsepower in $5^3/_4$-inch outside diameter up to 135 horsepower in $5^3/_{16}$-inch outside diameter, providing for all well depths down to 10,000 feet.

Power consumption is approximately 1/3 kilowatt hour per barrel of fluid produced per 1,000 feet lifted. The efficiency of the pumping unit is approximately 30 to 45 per cent.

Supplementary equipment includes a cable reel, a starting panel, and either a special transformer or a special a-c generator. About 900 volts must be available above ground to allow for the voltage drop in the cable leading to the motor.

Hydraulic Pumping

This method employs a double-acting deep-well reciprocating pump actuated by

Table III. Electric Power Consumption of Beam-pumped Wells

Group	Number of Wells Pumped	Weighted Average Approximate Depth of Wells in Feet	Barrels Actual Monthly Production: 0.85 Oil	1.12 Water	Total Fluid: Barrels	Specific Gravity	Actual Electric Energy Purchased, Kilowatt-hours	Actual Kilowatt-hours per Barrel of Fluid	Per Cent Over-all Efficiency Electric Meter to Fluid in Tank
A	6	2,210	2,610	30,900	33,510	1.10	30,300	0.91	35.4
B	59	2,540	35,000	121,000	156,000	1.06	187,500	1.20	33.8
C	11	2,750	19,980	8,760	28,740	0.90	27,068	0.94	35.0
D	29	4,186	37,004	136,390	173,394	1.06	321,960	1.86	31.6
E	21	4,238	29,490	63,300	92,790	1.03	181,997	1.97	33.8
F	66	4,260	54,203	92,852	147,055	1.03	237,744	1.95	33.9
G	15	6,430	36,300	1,950*	38,250	0.86*	103,000	2.70	27.0

*Specific gravity of salty water = 1.15 for group G.

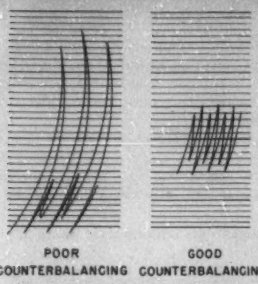


Figure 6. Power input to two beam-pumped wells, illustrating the effect of counterbalancing

a direct-connected power piston which is operated by oil under pressure. The pump is suspended in the well on tubing and submersed sufficiently to insure filling of the pump cylinder on each stroke. The oil to actuate the power piston is supplied by a triplex pump located above ground and driven by a squirrel-cage induction motor. This oil is pumped down to the power cylinder through a small string of tubing suspended inside the larger tubing, and is then discharged into the stream of oil produced from the well, which is pushed to the surface in the annular space between the two strings of tubing.

Equipment is available for pumping wells from 1,500 down to 10,000 feet on either single or multiple well hookups.

Power consumption is approximately 1.4 kilowatt hour per barrel of fluid for a pumping depth of approximately 6,400 feet, or 0.22 kilowatt hour per barrel per 1,000 feet of lift. The horsepower required depends on the pump size.

Secondary Recovery by Water-flooding

Water-flooding operations involve the injection of water at high pressure into the oil-bearing formation to force the oil to key wells, which are usually pumped by beam-pumping units or central "powers." The water supply is usually pumped from deep wells or lifted by compressed air. It is then chemically treated and filtered, pumped through a pipe line to the oil field, and forced underground by high-pressure injection pumps. The application of electric drives to these operations follows standard practice for the type of apparatus involved.

Control equipment for water-flooding systems must be completely reliable. Shutdowns must be avoided if possible, because they mean loss of production and possible detrimental effect on the progress of water flooding. Simple, automatic operation shortens many unavoidable shutdown periods, lowers the labor cost per barrel, saves time and reduces maintenance costs. The judicious employment of automatic motor starters, electric float switches, pressure switches and time switches will accomplish these purposes readily.

A few points deserve special mention. If an electric power failure stops a deep-well centrifugal water pump, the water standing in the vertical discharge pipe will rotate the pump backwards unless there is a ratchet head or other nonreversing device. If power is restored during reverse rotation, the pump shaft may be twisted off, but this can be obviated by a time relay which will keep the motor from starting until the pump has had time to stop running backward.

Injection pumps must be protected against loss of suction pressure. A pressure switch, operating on pump-suction pressure, is adjusted to trip the motor starter when the pressure goes below the permissible minimum. This prevents also the pipe line from filling with air.

Excessive voltage drop resulting from simultaneous automatic restarting of a large number of motors can be avoided by restarting in successive groups controlled by definite-time-delay relays. This avoids also the danger of collapsing the suction manifold of a group of pumps before the water has time to get to the pumps.

Repressuring

To maintain the underground reservoir pressure and thus prolong or increase well flow, natural gas stripped of condensates is in many fields returned under pressure to the oil producing formation. Usually steam is required in processing the gas, and can be used first to generate electric power. Before a specific plant is designed, a careful economic comparison should be made between the steam-electric and the gas-engine system, because the market value of excess gas is increasing, steam-

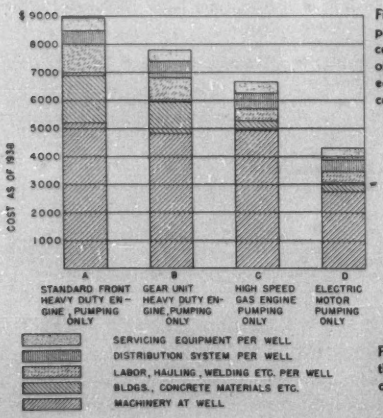


Figure 7 (left). Comparative installation costs of four types of beam - pumping equipment in mid-continent oil fields

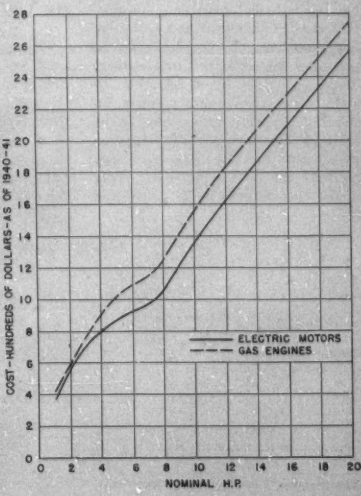


Figure 8. Comparative installation costs of beam-pumping units in Illinois

Table IV. Electric Power Consumption of Wells Pumped by Central Powers

Group	Number of Wells Pumped	Average Length of Pull Rods, Feet	Weighted Average Approximate Depth of Wells, Feet	Barrels, Actual Monthly Production: 0.85 Oil	1.12 Water	Total Fluid: Barrels	Specific Gravity	Actual Electric Energy Purchased, Kilowatt-hours	Actual Kilowatt-hours per Barrel of Fluid	Per Cent Over-all Efficiency, Electric Meter to Fluid in Tank
A	5	1,820	2,600	6,480	5,000	11,480	0.97	12,170	1.06	31.6
B	13	1,100	2,600	7,320	11,800	19,120	1.02	18,900	0.99	35.6
C	9	780	2,600	9,800	3,590	13,390	0.92	10,770	0.81	39.6
D	8	1,400	2,600	5,550	12,600	18,150	1.04	14,300	0.79	45.3
E	14	1,100	2,600	4,720	24,020	28,740	1.07	19,700	0.69	53.0
F	11	800	2,600	5,000	9,000	14,000	1.02	14,400	1.03	34.1
G	8	750	2,750	1,410	3,870	5,280	1.04	7,500	1.42	26.7
H	4	460	2,770	1,140	3,660	4,800	1.06	8,040	1.67	23.2
I	12	1,180	2,380	12,000	1,680	13,680	0.89	11,000	0.80	34.9
J	12	1,235	1,250	10,500	100	10,600	0.85	3,500	0.37	38.0

electric maintenance costs are comparatively low, and the demand for electrified auxiliaries is increasing.

A typical steam-electric plant uses steam-turbine generators, motor drives for compressors and pumps, cooling tower motors and control, auxiliary motors, switchgear, unit substations, auxiliary power transformers, and lighting equipment. Such a system is illustrated diagrammatically in Figure 11.

Electric Power Distribution for Well Pumping

Approximately two thirds of the cost of electrification of an oil field is for the power-distribution system; the remaining one third is for the motors and control. It is quite important therefore that the distribution system be laid out carefully, with due consideration of cost.

The factors which affect the layout and type of the distribution system are as follows: permissible voltage drop, cost of system, number and sizes of motors, well spacing, configuration of the field, pole spacing, size and type of line wire, power factor, and service reliability.

A system consists usually of 2,400-, 4,160-, or 13,800-volt primary feeders extending into the field, with transformers at strategic locations stepping down to 480 volts for supplying a group of wells. The load is much more widely distributed than in ordinary industrial plants, and each well is usually relatively small in horsepower requirements. The load distribution makes it necessary to run secondary feeders for as great a distance as voltage conditions will permit.

Voltage Drop

This is the main consideration in the layout. Motor current is commonly supplied at 440 volts over long feeders, with a low operating power factor, and voltage drop determines to a large extent the cost of the system. Good practice requires that the operating voltage, after the motors are started, should be not less than 95 per cent, nor more than 110 per cent, of the motor nameplate voltage. Although motors will operate at less than 95 per cent, and manufacturers guarantee noninjurious temperatures with operation at 90 per cent of motor nameplate voltage, a minimum operating voltage of 95 per cent allows some margin for the voltage drop occasioned by starting a motor. These limits of 95 and 110 per cent are permissible in oil field practice, though they have a slightly wider spread than generally recommended by the AIEE subcommittee on industrial voltage requirements.

The voltage at the motor during starting should be at least 80 per cent and preferably 85 per cent of the motor nameplate voltage. Flicker of field lights at the time a motor is started is not important.

In a well-planned system the voltage drops will ordinarily be from one to three per cent in the primary line, two to four per cent in the transformers, and $7^1/_2$ to $12^1/_2$ per cent in the secondary line. The total drop at the most remote well should be not more than 15 per cent of the no-load voltage, which will give approximately 95 per cent of motor nameplate voltage at the motor terminals when using a transformer tap to give 5 per cent above rated transformer voltage. If capacitors are used, the drop becomes approximately 10 to $12^1/_2$ instead of 15 per cent. Obviously secondary lines should be as short as possible. For this reason it is preferable to use the load-center principle of power distribution, in which power at primary voltage is brought directly to the load area before it is stepped down to distribution voltage.

If utility power is used, the primary voltage will be fixed by the utility prac-

Figure 9. Motor-driven geared pumping "power"

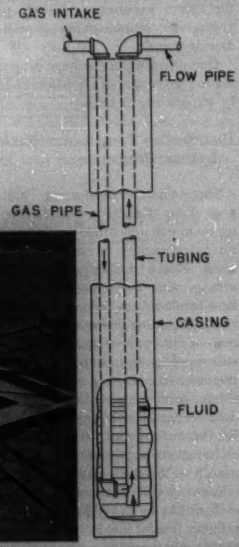


Figure 10. A simple form of gas lift

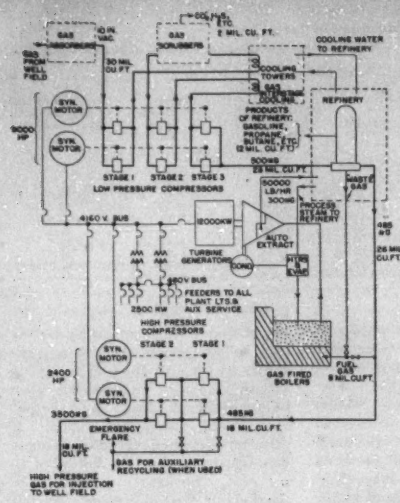


Figure 11. Steam-electric system for repressuring with natural gas

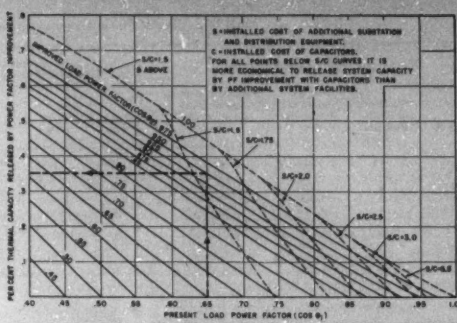


Figure 12. System thermal capacity released by improving load power factor

For example, if the power factor of a circuit is improved from 65 to 95 per cent, the system can carry 35 per cent more load having 65 per cent power factor without exceeding the original kilovolt-amperes, and capacitors are more economical than additional system facilities for all S/C values above 1.55

tice, but if it can be selected, 4,160 volts results in very much less drop than 2,400 volts, at almost the same cost, and allows for future expansion. Higher primary voltage are seldom necessary and is not as economical.

Compensation for voltage drop may be made by the use of transformer taps, or by capacitors, or both.

Distribution Transformer Banks and System Protection

Single-phase transformer units are preferable. Generally transformer banks up to 45 kva and sometimes up to 75 kva are pole-mounted on cross arms. Above these sizes a double-pole and platform arrangement is commonly used. The transformers are of standard design, readily obtainable, easily handled, require a minimum of spare parts, and can still supply power at reduced capacity in the event of outage of one transformer of a delta-delta-connected bank. The delta-delta connection is preferable, not only for this reason, but because standard 2-coil overload protection of motors can be used. A Y-delta or delta-Y connection requires 3-coil overload protection for each motor to avoid burnouts in case a primary fuse or line is open and the bank has an ungrounded neutral. In such a case double current will occur in one secondary conductor, and 2-coil overload relays will not then provide full protection.

Lightning arresters should be included at each substation for maximum protection. The 440-volt systems are invariably ungrounded, so secondary arresters should be selected for ungrounded service.

Fused cut-outs are economical and are employed almost universally to protect transformer-bank sizes normally used in oil-field distribution service.

If oil company practice permits the bonding of electric connections to the well casing, it will provide a suitable ground for the pumping-unit frame, motor-starter case and lightning arresters. The usual alternative is to use ground rods.

Capacitors

In a 440-volt system it is expensive to reduce the voltage drop by increasing the conductor size. Capacitors are the answer to that problem, by improving the normally low-power factor of well-pumping loads, which is approximately 60 to 65 per cent. The saving in system losses by the use of capacitors is often economically attractive, as the losses are inversely proportional to the square of the power factor. In oil-field distribution systems, with relatively long secondary runs, it is not at all unusual for capacitors to pay for themselves through reduction in system losses alone. Furthermore, they release substation and line capacity for additional load (Figure 12), or permit the use of smaller transformers and conductors, and will reduce the cost of utility power if the rate contract includes a power-factor penalty clause.

Capacitors can be connected either at the motor terminals or on the feeder lines. The advantages and disadvantages of both methods of connection should be weighed to determine the choice, which depends mainly on the ratio of the number of wells in operation to the total number of wells pumped. If all are pumped simultaneously, capacitors at individual motors are preferable, but it is uneconomical to use the outdoor type for motors under 10 horsepower. If only part of the motors are in operation at any time, the group-capacitor method should be considered because of the lower investment.

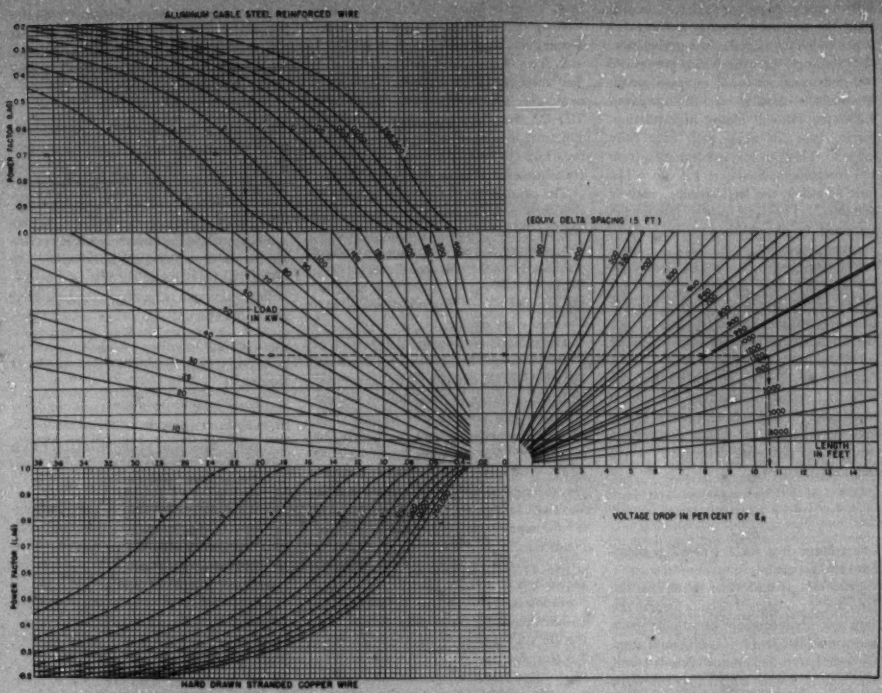
Table V

900 to 1,200 Rpm Motors

Type of Motor	Starting Power Factor
Normal starting torque motor	30 to 50 per cent
High starting torque motor	35 to 60 per cent
	Motor Inrush (Times full-load current)
Normal starting torque motor	5.0 to 6.5 (Usually 6.0)
High starting torque motor	4.5 to 6.0 (Usually 5.0)

Type of Line Conductor

In sour gas areas, steel-reinforced aluminum conductors are generally used instead of copper because aluminum is noncorroding. The spans between poles



LOAD MULTIPLIERS FOR DIFFERENT RECEIVER VOLTAGES

Receiver voltage...	404	408	412	416	420	424	428	432	436	440	444	448	452	456	460	464	468	472	476	480
Kw. multiplier.....	1.186	1.163	1.141	1.119	1.097	1.077	1.057	1.037	1.018	1.000	0.983	0.965	0.949	0.931	0.915	0.899	0.884	0.869	0.855	0.840

for 440-volt line construction can be from 300 to 500 feet with aluminum, compared with 150 to 350 feet with copper. Thus, the pole cost for aluminum lines is roughly half that for copper lines. Since pole cost is an appreciable part of the total cost, alumimum conductors represent a large saving. For equivalent conductor size, the voltage drop and wire costs are about equal. Greater care is required in making satisfactory splices and terminations with aluminum.

System Layouts

As complete information for making a layout of an oil-field distribution system has been published,[1] only an outline of the method will follow.

Calculation of the many voltage drops in a system is simplified by using the unit-load–unit-length (*UL–UL*) principle. The motor loads on a specific oil property are in most cases close enough to permit each to be termed a unit load. If not, the property can be divided into areas with about the same unit loads. Wells on an oil property are usually spaced uniformly, and the distance between two adjoining wells can be used as a unit length. The voltage drop is determined for this selected load and conductor length, and this value (*UL–UL*) is used as a base for other combinations of loads and distances. For example, the voltage drop for 2 *UL–UL* may be for one-unit load and two-unit lengths or for two-unit loads and one-unit length.

Figure 13. Voltage-drop chart for 440 volts

The kilowatt input to a motor, or the unit load, corresponds to the average load on the motor, which for a fully loaded motor for pumping-unit service is 60 to 70 per cent of the motor horsepower rating. The unit load may be expressed as follows:

$$\text{Kilowatt input} = \frac{(\text{horsepower rating}) \times 0.746 \times 0.65}{(\text{motor efficiency})}$$

in which 0.65 is the assumed loading factor.

The next step is to determine the power factor of the unit load from published charts[1] applying to the type of motor used, with or without capacitors at the motor, as the case may be. With this data the *UL–UL* drop is read from published voltage-drop charts,[1] such as the one shown in Figure 13 for 440 volts. If the load is small, the drop may be read for a larger load and reduced in direct proportion to the loads. The total secondary drop is obtained by addition of the drops for the various *UL–ULs*.

A simple example will serve to illustrate the use of the *UL–UL* principle. Con-

sider a single secondary feeder, extending from the transformer bank halfway between two lines of wells, two on each side, thus feeding four wells which are spaced 660 feet apart and are pumped simultaneously. Assume a 10-horsepower 1,200-rpm 440-volt motor, high-starting-torque type, at each well, each fully loaded. The unit load, determined from the foregoing formula, is 5.4 kw. The unit length is the well spacing, namely 660 feet. The load power factor, determined from published curves,[1] is 70 per cent, expressed as 0.7.

The voltage drop for a unit load of 5.4 kw at 0.7 power factor and one unit length of 660 feet may be read directly from the 440-volt chart (Figure 13) for the size and type of line conductor under consideration, say Number 4 ACSR (aluminum cable steel reinforced) but not with any degree of accuracy for such a small load. A higher load, such as 40 kw, may be selected, for which the voltage drop is 7.65 per cent. The drop for one unit load of 5.4 kw is

$$\frac{5.4}{40} \times 7.65 = 1.03 \text{ per cent}$$

The voltage drop for 1 *UL–UL* is therefore, 1.03 per cent.

From the transformer bank to the junction of the branch circuits feeding the first pair of wells there are 4-unit loads and 1-unit length (4 *UL–UL*) and from the junction to each motor there is 1-unit load and 1/2-unit length (0.5 *UL–UL*). The total secondary drop to each of the first pair of wells is therefore for 4.5 *UL–UL*, which is 4.5 × 1.03 or 4.6 per cent. From the junction for the first pair of wells to that for the second pair there are 2-unit loads and 1-unit length (2 *UL–UL*), and from the second junction to each well there is 0.5 *UL–UL*, making a total of 2.5 *UL–UL*. To these are added the 4 *UL–UL* from the transformers to the first junction, making a total of 6.5 *UL–UL*, for which the drop is 6.7 per cent from the transformers to the second pair of wells.

The primary drop can be calculated in the same manner, using a voltage-drop chart for the primary voltage involved. Transformer voltage drops are covered by published tables.[1] The total voltage drop in the system is the sum of the drops in the primary circuit, the transformers, and the secondary circuit.

The voltage drop during the starting of a motor should be checked for the final distribution layout to make sure that sufficient voltage is available at remote well locations to start the motor. The drop in the primary lines can usually be neglected. For the transformer bank, it can be read directly from a table,[1] and for the secondary lines it can be read from the chart in Figure 13. The data in Table V are representative for motor-starting conditions.

For estimating purposes the full-load motor kva is usually taken as 1 kva per horsepower. Considering, for example, a 15-horsepower high-starting torque motor, the starting kva = 15 (horsepower) × 5.0 (inrush) × 1.0 (kva/horsepower) = 75.0. For a starting power factor of 40 per cent, the starting kw = $\frac{40 \times 75}{100}$ = 30 kw. The secondary voltage drop for this starting load is read directly from the chart in Figure 13 for a power factor of 40 per cent. When capacitors are used, this voltage drop will be 90 to 95 per cent of the drop without capacitors.

Estimating Installed Cost of Distribution System

Data for this purpose have been collected and are available.[1] These data permits quick cost comparisons of various alternative layouts and are useful in arriving at the most economical layout. The estimated total installed cost of the distribution system is based on the following component parts:

1. Pole types.
2. Conductors, including stringing and sagging.
3. Transformer substations.
4. Capacitor equipment, if used.
5. Add ten per cent to above total for such miscellaneous items as surveying, trucking, and other incidental costs.

For a final reckoning of the construction costs, the services of a reliable oilfield contractor should be enlisted. It is well to keep in mind that the major economies in electrification of an oil property can be effected in the power distribution system.

Reference

1. Power Distribution Systems for Oil-Field Pumping, W. C. Bloomquist, J. N. Poore. The Oil Weekly, December 23, 1946, pages 46–50, December 30, 1946, pages 54–5, and January 13, 1947, pages 46–54.

Discussion

E. H. Lamberger (Westinghouse Electric Corporation, East Pittsburgh, Pa.): Taylor is to be complimented for presenting a paper which covers so many of the important items which must be considered when planning the electrification of an oil field.

The author's method of estimating beam-pumping horsepower requirements necessarily includes a formula for calculating the daily fluid production. One essential factor of the formula is length of stroke. Production is basically a function of the pump plunger stroke. This is usually not the same as the polished rod stroke due to the elastic characteristics of the rod string. The elastic characteristics involve such factors as length of rod string, speed of pumping, polished rod, stroke length, fluid load, and damping factor. The pump plunger stroke may be either greater or less than that of the polished rod. Obviously, it is desirable to use the actual pump plunger stroke, to assure highest possible accuracy of the calculated horsepower requirement.

We believe it would be well to call attention to a formula for calculating actual plunger stroke. This formula was given in an American Petroleum Institute paper in 1941.[1] The complete derivation of the formula is given in a paper[2] at the applied mechanics division meeting of the American Society of Mechanical Engineers in Cambridge, Mass., in 1942.

Basing the motor horsepower on hydraulic horsepower plus a value for subsurface losses is sound, and we believe should give more accurate results, for wells of modern straightness, than the commonly used method of assuming the friction losses to be some multiple of the hydraulic horsepower at the pump. This assumption results when the motor horsepower is derived by simply dividing the hydraulic horsepower at the pump by a factor representing some commonly assumed ratio of hydraulic to motor horsepower.

The formula given for subsurface friction horsepower results in a straight line relation between friction horsepower and speed of pumping. Since subsurface friction is a function of mechanical friction between rods and tubing, and two components of fluid friction, that of the rod string and fluid and that of the fluid and tubing, a curved relation, concave upward, is indicated. Such a curved relation is indicated by the formula for fluid friction because it includes a factor of velocity squared in the numerator. The curved relation has also been shown by careful tests. These tests, with a 3,500 foot string of 3/4-inch rods, showed 0.5 horsepower per 1,000 feet of string, at 1,000 inches per minute rod velocity, 1.2 horsepower at 1,750 inches per minute, and 2.5 horsepower at 2,500 inches per minute.

The author mentions 8 to 13 per cent slip, high-slip high-torque oil well pumping motors. We believe it should be stated that high-slip motors with a slip of 5 to 8 per cent have been and are being commonly and successfully used.

References

1. Oil Well Tests Suggest New Rating Standards, H. E. Dralle, E. H. Lamberger. *Drilling and Production Practice*, American Petrol

leum Institute (New York, N. Y.), 1941, pages 115–32.

2. Calculations of Load and Stroke in Oil Wells. Pump Rods, Langer, Lamberger. *Journal of Applied Mechanics*, (New York, N. Y.) volume 10, March, 1934.

Table I. Amplitude of Power Fluctuations in Circuits Supplying Beam Wells

Number of Wells	Type of Induction Motors: Slip	Hp	Rpm	Average Load Kilowatts	Per Cent of Amplitude from Average Load: Above	Below	Average Power Factor
3	Normal	75	1,150	107	32	17	0.67
12	Normal	15	560	105	41	34	0.53
9	Normal	15/25/40	1,750	130	57	65	0.73
8	High	40	1,160	219	21	20	0.65
65	High	15	1,160	240	22	22	0.60

W. H. Stueve (Oklahoma Gas and Electric Company, Oklahoma City, Okla.): This paper describes practically all the known methods of producing oil and the part that can be played by the electric motor application to the machinery required. Until recent years, the oil producing phase of the industry has been slow to adopt changes from conventional practices and sought maximum daily results regardless of the economics of its producing methods. The recent advent of the professional petroleum engineer into this business has changed the oil executives viewpoint to such an extent that, today, primary recovery methods are selected based on the mechanical and/or electrical efficiency of the equipment, with further consideration being given to correlating such primary methods with expected future secondary methods of recovery. This is important, for formerly only about 30 per cent of the oil in place in a given pool could be recovered with primary methods; whereas, an additional 20 to 30 per cent of all the original oil in place in a pool can be recovered now with secondary methods, such as gas repressuring or water flooding.

For this reason, it might be well if the efficiencies of the ten methods of production, shown in author's Table II, be given more consideration in the final analysis to determine the selection of the proper economic type of equipment. Figure 1 of the discussion is a study of almost 2,000 producing wells equipped with all types of pumps, motors (normal and high slip) covering the five general methods spoken of by the author, as adapting themselves to electric power. The "Max" line represents 28.5 per cent efficiency, "Mean" 36.3 per cent, and "Min" 48.7 per cent, all production being reduced to 1.0 specific gravity, and reading in kilowatt-hours per barrel versus feet pumping depth. Figure 1 of the discussion clearly points out the type of equipment which is the most efficient at the various pumping depths. Beam pumps and central powers, in some cases, are most efficient for wells approximately 3,000 feet deep; the submerged-centrifugal pump is most efficient for wells approximately 4,000 feet deep; and the hydraulic powered pump for wells 6,500 feet deep. It should be mentioned, however, that the most efficient method can not always be used, as shown in the limitations of capacity in Table II. Thus, we find that hydraulic powered pump will only produce up to 300 barrels per day at 6,500 feet depth; whereas, the centrifugal bottom hole pump, while not as efficient, yet it is the only pumping unit that can be used for volumes up to 1000 barrels per day from that same depth. This accounts for the 178 installations of the centrifugal bottom hole pumps as against only eight of the hydraulic powered pumps on Figure 1 of the discussion.

From the author's Table II of producing methods, no electrical records are available for the other methods, but, in general, it may be said that gas lift is only 10 per cent efficient and very expensive due to the great number of gas compressors required; further, governmental conservation commissions frown on this method since it takes production from surrounding leases whose owners may not ordinarily have the capital to make the more expensive installations. These same reasons apply to *4A*, the combination of *4* and *3*. In general, *4A* was used only to secure potentials of wells, and operated only in this manner for short periods of four to eight hours, so the method can not be construed as a permanent method. Note, however, that gas lifting in any form will produce large amounts of fluid. Pneumatic rod lift requires gas compressors and is only eight to ten per cent efficient, and is used only by producers already having on hand the necessary compressor equipment and having no other use for it at the time being.

The swabbing method is used only temporarily, perhaps during the first 10-to-20 days of the discovery of the well, while awaiting the permanent equipment. Note that the water flooding method from Table II for two groups of wells, is about 24 per cent efficient, 800 and 52 wells at approximately 600 feet depth.

The central powers method is efficient enough to meet most conditions, 30 to 40 per cent, except for limitations of depth being about 2,600 feet, and also relative small volumes of the order of 25 to 50 barrels of fluid per day. See author's Table IV, where average is 48.5 barrels per day from 96 wells studied.

It is true that about 90 per cent of all wells being pumped today are with beam pumps with individual power units (engines or motors) but the problem of rod breakage, and consequent loss of production while repairs are being made, has never been successfully solved. It is unfortunate that more electrical data are not available from the operation of the two types of submerged pumps, the centrifugal and hydraulic, from relative shallow depths below 4,000 feet, for it is felt that their use would prove more satisfactory and gradually solve the operator's problem of rod breakage by displacing the beam pump to a large degree.

The centrifugal unit now, however, is being made in small sizes of 5, 10, and 20 horsepower, especially for use in shallow wells, or wells of low volume. The hydraulic unit is now being promoted for shallow wells also, since the triplex pump supplying the power fluid is being centrally located between a group of wells, and the power fluid is distributed to individual wells in much the same manner as would be electric power.

Since 90 per cent of the oil wells are being pumped by the "beam pumps" method, the author had devoted a considerable portion of his paper to formulae for determination of horsepower required, and also to the selection of the type of motor to install for various conditions of wells. As a short cut suggestion to determine the motor size for a given condition, the average efficiency line from Figure 1 of the discussion can be used since it is plotted principally from points from the author's Table III. Thus, if the well is to produce 200 barrels of oil and water per day, from a depth of 4,400 feet then it will require two kilowatt hours per barrel of 1.0 specific gravity fluid or 400 kilowatt-hours in 24 hours. The average

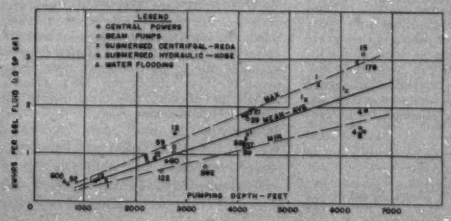


Figure 1. Electric power required to pump oil wells

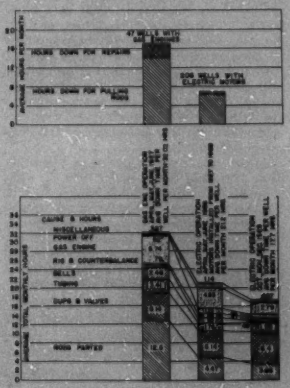


Figure 2 (right). Comparison down time for gas engines and electric pumping

electric load will be 16.6 kw input, or average 20 brake horsepower, requiring a 30 horsepower motor by applying the author's factor of 0.7 to compensate for peak loads during each stroke.

The author's Figure 3 clearly shows the effect of using normal slip (3 per cent) motors, in that peaks of 50 per cent greater than the average load can be encountered with only 12 wells in operation. In this case, the chart was moving at the rate of 12 inches per minute, and the change from 69 to 148 kw occurred in one second of time, which rapid change would impose a severe shock on any small generating equipment supplying the power. If high slip (10 per cent) motors had been installed on the 12 wells, the rapid load change would be reduced considerably and offer the small generating plant a better opportunity to carry the load. This reduction in peak loads achieved by high-slip motors for individual pumping units is best shown by the author's Figure 4.

When large groups of wells are considered, the effect of using high-slip motors is more pronounced, as shown in the Table I of the discussion.

A study of Table I of the discussion reveals that a more even power flow usually will be obtained from groups of wells equipped with high-slip (10 to 12 per cent) motors. While the normal-slip motor may have a lower first cost, yet the greatest cost of the high-slip motor may be compensated for to some degree in less rod breakage and, therefore, prove more economical in the long run. Also, note that the power factor of the normal-slip motors is considerably improved as the speed of the motor is increased. This is important to the oil producer since the higher speed motor generally is cheaper in cost and savings can be effected in transformer capacities and distribution system wire sizes. The data from this Table I bear out the author's opinion that more consideration should be given to using higher slip motors wherever possible.

The author's discussion of determination of wire sizes, distribution voltages, and the use of capacitors are well worth studying and following out by the design petroleum engineer in laying out the power supply facilities.

The author's statement relative to the pumping methods employed being something greater than 90 per cent with beam pump is correct, and since the greatest difficulty with this method is shutdown time on account of rod breakage, it is believed more information on this subject would have been interesting to review. For this reason, Figure 2 of the discussion is submitted to show the relative shutdown time for several groups of wells, for gas engine and electric motor operation.

These data cover a period of operation from April 1927 to December 1929, for wells of approximate depth of 2,800 feet located in Texas, equipped originally with single-cylinder horizontal gas engines and operating 24 hours per day. In order that proper comparisons could be made, three 3-month periods were selected and the results reduced to average monthly basis as shown graphically in the lower part of Figure 2 of the discussion.

The first period covers the operation of 109 gas engines for April, May, and June 1927. Following this period, the operations of the same wells were gradually being changed to electric-motor drive until April 1928, at which time there were 117 wells being pumped with electric motors. Therefore, the second period covers April, May, and June, 1928. One year's time elapsed in this transition period from gas engines to electric motors. The third period covers the operations during October, November, and December, 1929, and involves a study of 167 wells equipped with electric motors. The elapsed time was $1^1/_2$ years, between June 1928 and October 1929, which should be sufficient for the operators to become familiar with the new equipment. Only four gas engines were in use after April 1928.

It will be noted from Figure 2 of the discussion that the total down time per month per well was 32.02 hours for the first period, 21.2 hours for the second period, and 17.7 hours for the third period. The total down time for each well was broken down into down time for the various causes.

Under the cause "rods parted" the down time decreased from 12.5 hours per well per month, during the first period, to 4.47 hours during the second period, and 3.66 hours during the third period, the decrease probably due to being able to effect more accurate counterbalancing with the electric motors.

Another large oil-producing company submitted data regarding 47 single-cylinder, slow-speed gas-engine-driven wells and 206 electric-motor-driven wells located in the same field in Texas, pumping from a depth of approximately 3,000 feet.

These data are not carried out in detail for shut-down time, as shown in the lower part of Figure 2, but cover a period of several years from 1928 to 1930. The results of this study are averaged by the month and are shown in the upper part of Figure 2. Note that the hours "down time for pulling" appear in about the same proportion for gas engines and motors, as appear in the lower part of the figure under the caption "rods parted and tubing troubles."

The net results of both studies go to show advantages of the smooth electric-power application over the more or less pulsating power of the gas-engine application.

It is hoped that this discussion will in no way detract from the fine presentation made by the author in which he portrays the electric power uses for production of oil primarily from the equipment manufacturer's standpoint. Information regarding oil company operations, as submitted in Figures 1 and 2 of the discussion, is very hard to obtain, first, because they do not keep detailed records, and, second, because they do not care to expose their operations to the scrutiny of other people in their same business. For this reason, some of the information supplied in this discussion may appear too old, yet, in principle, the data are as good today as they were when taken, for the reason that production practices have remained rather static over long periods of time.

W. G. Taylor: Lamberger has called attention to two excellent refinements in the suggested methods of calculating well production and horsepower requirements. In consideration, however, of the facts that the results may be materially affected by the unpredictable amounts of sand and gas produced by the well, and that the assumption of volumetric efficiency of the pump may not always be close, it is questionable whether these refinements usually are warranted. No doubt the length of the stroke is the more important of the two, and although the production formula does not state this specifically as pump plunger stroke, that is the factor to use when data are available to determine it.

It is desirable that formulas for quick estimates be fairly simple. Great accuracy in this case would be inconsistent with the facts that some influencing factors are practically impossible to evaluate and that the standard motor size which must be selected is in most cases somewhat larger than the horsepower calculated.

Regarding induction motors with a slip of 5 to 8 per cent, commonly known as medium-slip motors, these are mentioned at the end of the section "Guide to Motor Selection for Beam Pumping." They came on the market for oil-well pumping at lower prices than high-slip motors of the same ratings, and have been attractive to the oil companies on that account. That they have been and are used successfully does not prove that they are the best motors for the purpose. The purpose of the slip is to utilize the stored energy in the rotating parts of the rig and motor to carry part of the peak load and thus reduce the current peaks drawn from the power system. As this stored energy is small, most of it being in the rotor of the motor, it is logically of most advantage to use as much of it as is possible and this is done by using the highest practicable motor slip. It is only by comparison that the user can readily appreciate the difference. However, there are probably cases where the medium-slip motor does well enough to satisfy the user, even if he knows the difference.

Stueve has added very materially to the information in my paper by the data and discussion which he has presented. It will be of great value to those interested in the subject.

Almost certainly a gas-engine man would claim that Stueve's data on engine shutdowns and rod breakage apply to a practically obsolete engine, which is true, but no data which I have been able to obtain on the high-speed multicylinder type of engine, now more generally used, indicate that any materially better results can be expected. Note particularly that Stueve points out that decrease in rod breakage with electric drive is probably due to the ability to effect more accurate counterbalancing with electric motors, and of course this has no connection with the type of engine used.

Full Utilization of Mexico's Hydraulic Resources Through Multiple Purposes

A. ORIVE ALBA
NONMEMBER AIEE

Available Water and Land in Mexico

CONTRARY to the current idea that Mexico is a country rich in natural resources is the fact that it is extremely poor in the two replenishable natural resources most vital to the economic development of any nation. These two resources are water and productive soil.

Rainfall is so scanty in 52 per cent of her territory that there exists no possibility of agricultural development without recourse to irrigation. Those parts of the country are referred to as arid zones. Rainfall in 31 per cent of the territory is distributed so unevenly that only occasional harvests are obtainable, thus requiring irrigation to assure them. Rainfall is sufficiently steady in 10 per cent of the remaining regions to assure agricultural development during most years. However, irrigation also is necessary and desirable in those regions to assure harvests during years when rains are scanty and to increase crops when the rainfall is abundant since, quite frequently, precipitation is not always even throughout those parts.

Summing up, we come to the conclusion that 93 per cent of our territory requires irrigation to guarantee agricultural development. Only in the remaining 7 per cent, which corresponds to tropical coastal sections of the states of Veracruz, Tabasco, and parts of Campeche and of Chiapas, are annual rains sufficiently abundant to assure agricultural production without recourse to irrigation. (See Figure 1.) These humid zones, potentially rich from an agricultural standpoint, are, however, unhealthy. To be able to explore and make them habitable, costly works in sanitation and drainage must be undertaken.

Mexico's mountainous topography, her climate and the quality of her top soil, in combination with factors of lesser importance, make it possible to estimate the amount of land that could be cultivated. It is estimated that this land does not exceed 60,000,000 acres (24,000,000 hectares) which represents 12 per cent of our total territory. However, harvests can be guaranteed only in a greatly reduced area which may be estimated reasonably as follows:

In 93 per cent of our territory where irrigation is necessary and which embraces the arid, semiarid and semihumid zones, the total utilization of river water in those parts permits only the irrigation of 17,500,000 acres (7,000,000 hectares) of land.

In the humid tropical zones which comprise 7 per cent of the nation's territory and where rainfall, as mentioned, is so abundant, the most optimistic estimates permit the conclusion that, after carrying out works of sanitation and drainage, only 7,500,000 acres (3,000,000 hectares) of land could be actually cultivated.

As a consequence, the estimated 60,000,000 acres (24,000,000 hectares) of land possible to cultivate must be reduced to 25,000,000 acres (10,000,000 hectares), leaving 35,000,000 acres (14,000,000 hectares) subject to cultivation only under the most fortuitous conditions, excepting such land as might be irrigated through the use of subsoil water. As yet, no close appraisal has been made of the extent of land that could be irrigated in this manner, but is estimated this will prove to be but a very small part of the total.

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A. ORIVE ALBA is Minister of Hydraulic Resources, Mexico, D. F., Mexico.

The Institute is indebted to the Mexico City Section for the translation of this paper from Spanish into English.

Significance of the Insufficiency of Water and Productive Land

The data just given are of vital significance to Mexico, a country that has at the present time 23,000,000 inhabitants with an annual population increase of a half a million. Assuming that all of Mexico's hydraulic resources were being fully utilized and that all requisite sanitary installations in tropical zones were already built, the nation would have only 25,000,000 acres (10,000,000 hectares) of land capable of cultivation, which means there would then be a coefficient of 1.07 acres (0.43 hectare) of productive land for each inhabitant. In the United States with a population of about 140,000,000, there are 400,000,000 acres (160,000,000 hectares) of productive land. The coefficient of such land is therefore nearly three acres (1.2 hectares) per person. This comparison indicates the inferiority of Mexico's agricultural possibilities in relation to those of the United States. To this disproportion we must add a consideration of the technical progress which has been made in agricultural development by the United States, itself an assurance of superior production per cultivated acre than that now obtainable in Mexico.

As has been indicated, the coefficient of 1.07 acres (0.43 hectare) per inhabitant constitutes the goal to be reached only when all the numerous installations necessary to a full use of Mexico's surface waters for irrigation purposes have been effected. At the present time, these installations are far from completion, thus the coefficient of land presently cultivated is considerably less than that of the aforementioned assumption. Actually, only about 5,000,000 acres (2,000,000 hectares) are now being cultivated through the use of irrigation and 15,000,000 acres (6,000,000 hectares) watered by rainfall. The total agricultural production of these lands is inadequate to meet the demands of the population, even of those basic food products such as corn and wheat. This deficiency in itself explains the low level of existence of the inhabitants of Mexico.

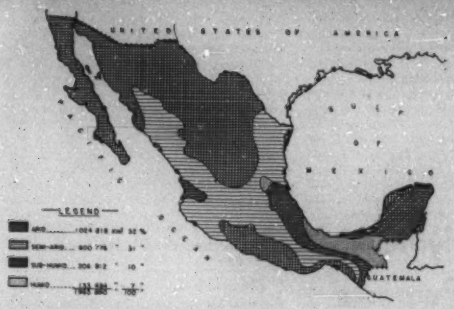


Figure 1. Climates of Mexico

Planning the Economic Development of Mexico

The government of Mexico, aware of the necessity to improve the food situation for the nation's population, has placed special importance upon the execution of great irrigation works. President Miguel Aleman has proposed the expenditure of 1,500,000,000 pesos, ($300,000,000, American dollars) for the construction of such irrigation works during his six year term of 1946 to 1952. This proposed expenditure will permit an increase of 2,500,000 irrigated acres (1,000,000 hectares). To appreciate the ambitious extent of the goal which the Mexican government has set itself to reach in the matter of irrigation, compare it with the history of the United States Bureau of Reclamation. Founded in 1902, during the first 30 years of its activity, the bureau had not spent $300,000,000 on irrigation, an amount less than 1,500,000,000 pesos, and, moreover, the land made available for cultivation by the bureau was less than that which our government intends to develop during the present presidential term of six years, terminating in 1952.

President Aleman has proposed projects for hydraulic and agricultural developments throughout all of Mexico, for the most part on arid land of insufficient agricultural productivity, in accordance with the following:

An increase of agricultural production of the nation to the maximum point compatible with her natural resources of water and land, to the end of meeting the demands of internal food consumption. The country should export, in some instances, tropical agricultural products, the volume of which exceeds the demands of internal consumption.

As Mexico can never hope to be a great agricultural country because of her paucity of water and productive soil, she should undertake through adequate co-ordination the exploitation of her other natural resources which exist in abundance, and fully develop the use of her raw products. This is imperative because the industrial development of the nation is its only possibility to establish an economically stable future.

These two purposes, harmoniously combined, provide a basis for defining the hydraulic policy of Mexico and should be the same for any country whose physical conditions are similar to our own. This definition of our hydraulic policy may be stated as follows:

All river water that flows through arid, semiarid, and semihumid regions of Mexico should be utilized to irrigate the greatest possible amount of land. It should be considered a grievous and inestimable loss to the nation to allow any of the river water that flows through land capable of cultivation through irrigation, to escape unused to the sea.

By taking advantage of dams and reservoirs and other installations to regulate the flow, the water of rivers should be used whenever possible to generate the maximum amount of electric energy before it is used for irrigation purposes, for, realizing the necessity of an industrial future for Mexico, we know that the best way to develop it is to enable the entire country to count upon an abundant supply of cheap power.

Examples of the Application of Our Hydraulic Policy

Arid, Semiarid, and Semihumid Zones

The aforementioned principles have served as a guide to formulate the projects we are carrying out to make full use of our river water, and to illustrate examples of what we are doing, we mention the complete utilization of the waters of the Cupatitzio river, which is a project of the Tepalcatepec Basin Commission, a branch of the Ministry of Hydraulic Resources.

This river has its source at springs located near the township of Uruapan, Michoacan, in western central Mexico. (See Figure 5). It has a uniform run-off, thus full use may be made of its water without recourse to reservoirs. By following the diagram of Figure 2 it can be observed that from its source to a point where it empties into the Casilda canal, the water level drops off approximately 3,610 feet (1,100 meters). Of this drop, 676 feet (206 meters) are already being used by the Cia Electrica de Morelia, and by the Federal Commission of Electricity, leaving a drop in the level of 755 feet (230 meters) which exists between an elevation of 4,540 feet (1,300 meters) above sea level at the foot of the Tzararacua Falls, and an elevation of 3,760 feet (1,150 meters) at a selected site for the construction of a diversion dam, for the installation of a hydroelectric plant in the near future. The magnificent natural beauty of the Tzararacua Falls precludes any industrial use being made of them as long as no imperative collective need exists, which at present is considered to be quite remote.

By constructing a diversion dam at a point called La Isla, the first step shall

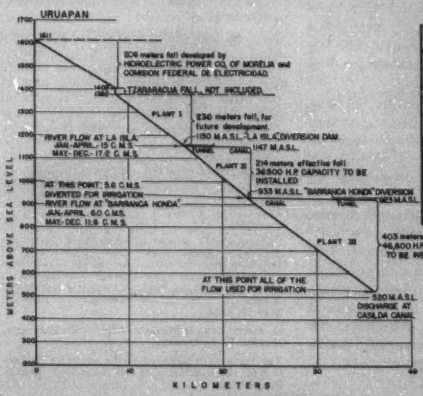


Figure 2. Tepalcatepec River Commission Cupatitzio Development

have been taken to utilize the water of the Cupatitzio river. The elevation at La Isla divides in half the existing level between the Tzararacua Falls and Barranca Honda. Reserved for the future installation of Plant I, is the 755 foot (230 meter) drop in level already mentioned and which is shown in the diagram of Figure 2. By diverting the water at La Isla to Barranca Honda by means of a tunnel and a canal, a fall of 705 feet (214 meters) in the level can be utilized. With a normal run-off of river water of 606 second feet (17.2 cubic meters per second) it will be possible to develop 36,500 horsepower. Hydroelectric Plant II will be constructed there and will be a twin of Plant I.

Water discharged by this plant, which will be 198 second feet (5.6 cubic meters per second), will be used to irrigate important nearby agricultural zones. Drops in the levels of the irrigation canals will be used to operate secondary generating plants. A diversion of the water furnished by the diversion dam at Barranca Honda by means of a canal and a tunnel to El Salvador Barranca, will make it possible there to take advantage of a drop in level of 1,320 feet (403 meters) and, with a river flow reduced to 410 second feet (11.6 cubic meters per second), to develop 46,600 horsepower at Plant III. The water of this plant will empty into the Casilda canal and, together with the water of La Parota river and that pumped from the El Marquez river utilizing the power generated by Plants II and III along the Capatitzio river, it will be possible to irrigate 62,500 acres (25,000 hectares).

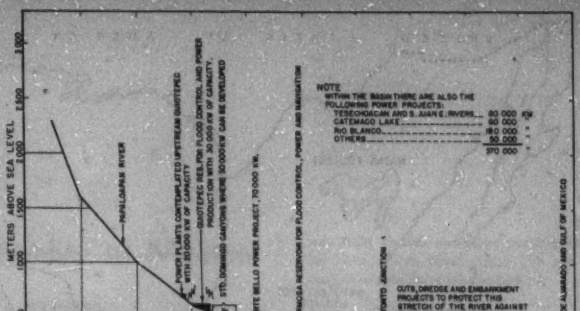
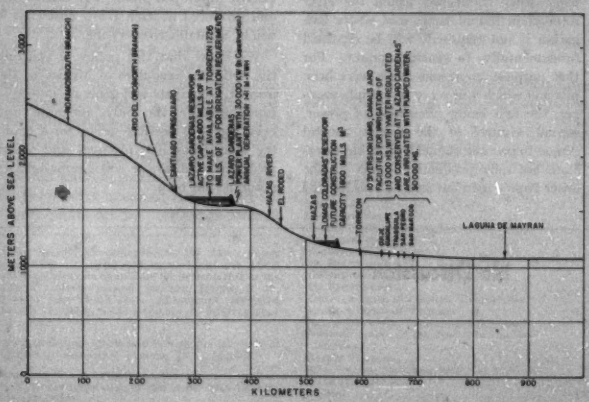


Figure 4. Papaloapan River Basin (Comision Del Papaloapan)

This project to utilize the Cupatitzio river water will permit the total utilization of that river's water together with that of other rivers and thus prevent any of the water to flow unused to the Pacific Ocean through the Balsas river. This example briefly represents the total utilization of water of a single Mexican river. An investment of 45,000,000 pesos (9,000,000 dollars) will be required to carry out this project but the following results will be obtained:

Irrigation of 62,500 acres (25,000 hectares) of rich soil and generation of 231,000,000 kilowatt hours. The annual return from the sale of power is estimated at $1,800,000 (9,000,000 pesos) and this alone would justify an investment of $14,000,000 (70,000,000 pesos). It may be concluded therefore that the project will not only pay for itself from a hydroelectric standpoint but will cover the necessary investments required for agricultural developments in that region, constituting thereby a group of works that realize both of the major objectives of the Ministry of Hydraulic Resources.

Figure 3. Nazas River Development



Another example that demonstrates how Mexican rivers may be utilized is the plan for taking advantage of the Nazas river in northern central Mexico (see Figure 5). The Nazas has its origin at an approximate elevation of 8,230 feet (2,500 meters) above sea level at the junction of the Ramos and El Oro rivers. Through the construction of the Lazaro Cardenas Reservoir which has a total capacity of 2,485,000 acre-feet (3,080 million cubic meters) and an active capacity of 2,105,000 acre-feet (2,600 million cubic meters), 80 per cent of that river's runoff is stored.

In this reservoir, the stored volume of water will permit the generation of 141,000,000 kilowatt hours once the 30,000 kw power plant, now being installed, is completed. At present, the water released from the reservoir is being used to irrigate 280,000 acres (113,000 hectares) of land in the region popularly known as La Laguna. The power generated at El Palmito hydroelectric plant will be used in part to pump sufficient water to irrigate an additional 125,000 acres (50,000 hectares). In this manner, all of the water of the Nazas river, before it reaches Lake Mayran is today being utilized to develop agriculture and to generate power. These installations will be perfected, through the construction of the Lomas Coloradas dam below the waters of the Lazaro Cardenas dam, a project planned by the

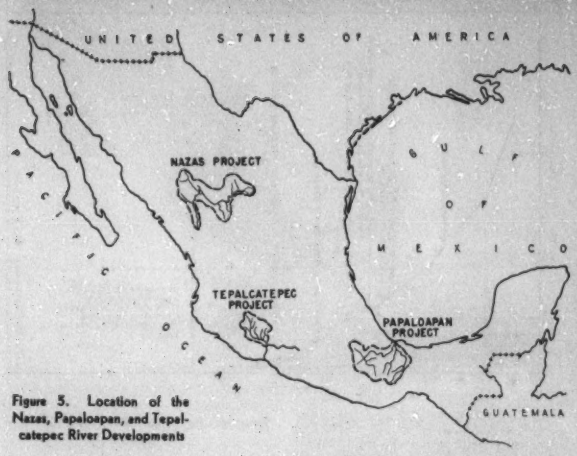


Figure 5. Location of the Nazas, Papaloapan, and Tepalcatepec River Developments

Ministry of Hydraulic Resources (see Figure 3).

The Lomas Coloradas dam will permit the hydroelectric plant at El Palmito to generate annually 150,000,000 kilowatt-hours in accordance with the demand made by the power companies in the States of Chihuahua, Coahuila, and Durango. Water discharged from El Palmito dam and Lazaro Cardenas reservoir will be stored and discharged later into the Lomas Coloradas reservoir as may be required for irrigation. Thus, full use of the water of the Nazas river will be made.

Utilization of River Water in Humid Zones

With regard to rivers that flow in humid, tropical regions where irrigation is not required, factors which define our hydraulic policy are reversed, for as no water is required to irrigate and, as in those regions there exists a marvelous perspective of securing a wealth of raw products, the first objective of installing hydraulic works should be the generation of maximum electric power and likewise, the other important purpose of controlling river courses through a regulation of the flow to ward off floods which often devastate whole sections of that part of the country when water is disproportionately abundant.

A good example in this connection, is the case of the Papaloapan, one of the great tropical rivers of Mexico. There is a project now under way, referred to as the Mexican Tennessee Valley Authority project, in charge of the Papaloapan Commission, another branch of the Ministry of Hydraulic Resources, which has as its aim to make use of that great river's waters.

Water of its tributaries which have their source in the arid parts of the states of Oaxaca, Veracruz, and Puebla will be used to irrigate agricultural lands of those states, but such water as is not used in this irrigation, as well as the water from other tributaries which the river receives in humid zones and where irrigation is not required, will be destined fundamentally, to generate power. For that purpose, enormous dams have been planned which will serve in a triple manner, the first being generation of power, second, control of the river's natural course to prevent annual floods that devastate not only productive lands of the lower Papaloapan but also villages located along its shores, and third, to facilitate navigation along the river (see Figure 4)

The basins of the rivers described may be seen schematically located in the map shown in Figure 5. Examples mentioned constitute but a part of the innumerable works being developed throughout the entire Republic of Mexico by the Ministry of Hydraulic Resources.

Objectives of the Ministry of Hydraulic Resources

So that the hydraulic policy, so tremendously important to a nation like ours, might better be materialized, President Miguel Aleman, at the time of his inauguration, created the Ministry of Hydraulic Resources, stressing clearly and precisely that such a policy cannot be eluded due to Mexico's physical reality.

Each of the examples cited is typical of what the nation is doing and proposes to do with every one of its rivers where hydraulic projects are being installed or where they should be executed. The most careful and conscientious studies are being made to improve the utilization of replenishable natural resources invaluable to Mexico due to her paucity of water. Irrigation and generation of power are considered to be the foundation upon which to build a glorious future for our country.

This desired goal will be reached when sufficient agricultural production has been assured to meet all achievable needs of the population. It will be possible only through co-ordination of the production obtained from reclaimed land with that of industry made vigorous through the abundant supply of hydroelectric developed power, well distributed and produced at such a low cost that it will be available to everyone.

We know that this policy of utilizing the hydraulic resources of Mexico to the utmost, to irrigate and generate power, is fundamentally the only adequate and even the only possible policy for a country that faces like physical and water problems, and whose soil characteristics resemble our own.

No Discussion

Hydroelectric Potentiality of Mexico

ANDRES GARCIA QUINTERO
NONMEMBER AIEE

THE amount of hydraulic power available in Mexico is ascertained by taking two essential factors into consideration, available water and waterfalls. The surface hydraulic resources of Mexico have been estimated to be 183,000 million cubic meters. This estimate is based upon the hydrometric measurements made by various government departments of basins that cover an area representing 38 per cent of the total area of the country. The major part of these basins is located in desert regions and in arid and semiarid sections. To estimate the mean annual runoff in humid regions, rainfall data, runoff coefficients, and areas of basins have been used and, to have a basis for comparison, some of the registered water measurements taken of the Usumacinta, Papaloapan, and Suchiate rivers. These data have proved of inestimable value in securing a truer conception of the water resources in those areas.

The other important factor or the difference of the levels that exist along the trajectories of our rivers until their waters are discharged into the sea or reach their final destination, compensates in part for the scarcity of water, for it is a well-known fact that the orographic formation of Mexico is characterized by the high central plateau which is bounded on the east by the Sierra Madre Oriental and on the west by the Sierra Madre Occidental while its southern tip is closed in by the massive mountain range called the Sierra Madre del Sur. Although some of the principal rivers of the country have their source in this plateau, the volume of water that flows along their trajectories is relatively small and nearly always of no practical value whatsoever during the low-water months of the year.

As precipitation is heaviest along the flanks of the mountains that face the oceans, rivers with the greatest amount of water have their headwaters there and, as these mountains are steep and often reach altitudes of 2,500 meters or more, extraordinary waterfalls are formed by the rivers that course down their sides to foothills and coastal plains averaging 200 meters above sea level. The volume of water of these rivers combined with their waterfalls creates enormous possibilities for hydroelectric development and generation of millions of kilowatt-hours.

Factors That Restrict Generation of Power

There are a number of factors that restrict the generation of electric power through the use of the Mexican river waters. They are:

Hydrological. In the first place, as mentioned, the flow of the rivers through the central plateau is very erratic. The same may be said of the flow of all rivers, for they derive their water in the main from the topographical rainfall. Storms bordering on cyclones frequently cause heavy rains and in the northwestern regions of the country, hot and cold masses of air during some of the winter months cause heavy precipitations. As a consequence, reservoirs have been found to be indispensable to regulate the natural flow. By storing up sufficient water during the rainy season, provision is made to regulate the flow during the low-water months of the year.

Table I. Power Installations Constructed in Mexico During Different Years

Year	Hydroelectric Plants, Kilowatts	Thermoelectric Plants, Kilowatts	Total, Kilowatts	Reference
1930	262,200	201,400	463,600	Herrera y Lasso
1939	389,100	291,300	680,400	General Director of Electricity
1943	397,100	283,000	680,100	General Director of Electricity
1946	463,100	330,800	775,900	Various
1948	511,000			

Table II. The Hydroelectric Potentialities of Mexico

Hydrographical Regions	Surface Runoff, Millions of Cubic Meters	Horsepower Potentialities	Geographical Distribution, Per Cent
Lower California	500	10,621	0.08
North Pacific region	21,483	4,573,391	34.71
San Pedro-Mezquital	3,150	795,818	6.04
Lerma Santiago basin	8,680	2,192,457	16.64
Central Pacific region or Southern Jalisco	5,630	187,477	1.42
Rio Balsas basin	8,650	288,029	2.19
Southern Pacific region	8,233	222,197	1.69
Rio Bravo in Mexico	5,223	227,471	1.73
Northeastern region (Northern Gulf)	4,390	93,259	0.71
Rio Panuco basin	6,210	320,878	2.44
Northern region of Veracruz (Central Gulf)	8,230	2,078,961	15.78
Southern region of Veracruz			
Papaloapan basin, Tabasco, and Campeche	95,510	2,062,658	15.65
Enclosed basins of the Northern Central Plateau	227	1,823	0.01
Central Plateau	1,888	52,034	0.39
Other regions including valley of Mexico and Suchiate River	1,440	68,954	0.52
Total	181,294	13,176,016	100.00

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ANDRES GARCIA QUINTERO is with the Ministry of Hydraulic Resources, Mexico, D. F., Mexico.

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Second, since it is not economically feasible to construct dams to store up all the water of the rivers when rainfall is heavy, much of it is lost through spillways, a fact to which one has to be resigned. Evaporation losses furthermore reduce the disposable volume. Thus, only a maximum of 75 to 80 per cent of the natural runoff can be utilized.

Topographical. Though differences of the levels of the Mexican rivers are often tremendous along short, steep stretches, these differences unfortunately cannot be utilized 100 per cent, as dam construction requirements, reservoir capacities, canals, and derivations cause significant reductions of the total head. The gross potential head is reduced furthermore by friction losses, intake, elbow, and pipe losses. It is therefore estimated that only 50 per cent of the available total potential head can be utilized.

Other Restrictions. There are other factors that contribute to the reduction of the theoretical power which could be developed through the use of river waters, such as the combined efficiency of hydraulic and electrical equipment, demand characteristics and length of transmission lines. An average efficiency of 75 per cent is what may be expected when all these factors are taken into consideration.

How Mexico Has Utilized Her Hydroelectric Resources

Mexico has just started to develop her potential hydroelectric resources. In fact, as will be observed, Mexico has utilized to date less than one-tenth of all her hydroelectric resources. Taking into consideration available statistical data, the chronological summary of hydraulic potentialities shown in Table I was drawn up.

Hydraulic Power Available in Mexico

The only logical way to estimate the hydroelectric potentialities of Mexico is to estimate and directly measure her surface water resources, to precisely know her topography in order to determine what the most feasible sites are where power can be generated. The generation of power then is subject to:

1. Storage of water in reservoirs.
2. Capacity of reservoirs.
3. Conduction of water to site of utilization.
4. Construction of the power house.

It should be remembered that Mexico is a country so deficient in water that she requires, for an assured agricultural development, the irrigation of her arable lands in 93 per cent of her total area. This leaves only 7 per cent of the territory in which natural humidity makes irrigation unnecessary in the arable land, and where it is possible therefore to employ all the surface waters to generate power.

Experience gathered through our work as hydrologists in the Ministry of Hydraulic Resources allows us to make a tentative estimate of the magnitude of hydroelectric potentialities of Mexico. This estimate is shown in Table II.

Table II shows that it is possible to dispose of approximately 13,176,000 horsepower of potential water power in the whole country. However, taking into consideration the reduction of water resources through the continual increased utilization of water for irrigation and other purposes, we must acknowledge that only about 9,785,500 horsepower are available for hydroelectric development or 25 per cent less than the estimated potential total.

Table III. Horsepower Potentials of Several Countries

Country	Horsepower Potentialities
Africa (Belgian and French Congos)	124,664,879
United States of America	41,689,008
India	26,809,651
China	20,107,239
Canada	17,963,466
Russia	15,951,743
Mexico	9,785,523

Mexican Resources as Compared with Those of Other Countries

In order to gain an insight into how the Mexican hydroelectric potentialities compare with those of other nations, their estimates are given in Table III.

At present, Mexico's hydroelectric installations total a capacity of 511,000 kw; with a mean annual demand of 250,000 kw. As the estimated total potentiality amounts to 9,785,523 horsepower which will represent a mean annual demand of 7,300,000 kw it is deduced that Mexico up to the present has only developed but 3.4 per cent of her hydroelectric resources.

Required Installations

Considering that two kw need usually to be installed to take care of an average one kw demand, installations totalling 14,600,000 kw are required to take full advantage of Mexico's hydroelectric potentialities. As only 684,987 horsepower have been developed, there remain yet to be built, installations with a total capacity of 18,886,059 horsepower to supply the balance of 14,089,000 kw. It is estimated that a capital investment of 9,862,300,000 pesos or $2,000,000,000 (American dollars) will be required to construct these installations.

No Discussion

Resistance-Welding Machine and Power Supply

C. E. SMITH
MEMBER AIEE

THE PURPOSE of this paper is to acquaint the reader with the various types of resistance-welding machines, their correction devices, and their effects upon the power supply systems.

The installation of these various types of welding machines is dependent upon the following.

1. The ability of the machine to be installed without creating a cyclic flicker disturbance on the high line regardless of the customer's present power-supply system.

2. The effects of the installation in consideration of over-all cost with a thorough study of the type of equipment which would prove the least expensive. The cost of the proper distribution system, if the high-line supply is inadequate to take the instantaneous demand created by the various types of welding machines, must be analyzed carefully against the cost of individual correction devices.

A thorough study must be made to each individual installation or proposed installation as to the proper types of machines and the cost of these machines to produce an installation of the most economical value. Therefore, the cost of these various systems known at the present time must be given consideration.

3. Various correction devices considered in this report with their departure from the fundamental wave shape of 60-cycle current or supply-line current and the effects of various secondary wave shapes in the welding of the material are given consideration.

It appears at this time that the single-phase a-c machines, uncorrected, are the machines for general application wherever the power supply is adequate. In other words, if the installation of a group of a-c, uncorrected, single-phase machines could be installed without creating cyclic flicker nuisance values, the a-c single-phase machine is still the machine to recommend. Wherever the power supply is inadequate to handle regulation imposed on the system, a thorough study must be made according to the number of machines being installed and the present cost of reinforcing the system versus the various types of direct and indirect-energy types of machines available at the present time.

When large and intermittent single-phase loads produced by resistance-welding machines are taken from a 3-phase system, an unbalanced current distribution results which is generally undesirable.

1. Lowered apparatus economy of transformers, lines, distribution system, and generator.

2. Lowered over-all efficiency.

3. Line voltage distortion, or unequal voltage regulation resulting from unequal loading on the three lines.

Various transformer connections and other electric equipment have been proposed in the past either to lower the kilovolt-ampere demand of the machines, equalize the distribution of the single-phase current over a 3-phase system, or both.

For the purpose of readily comparing one or more of these systems with another, they have been arranged in two groups, direct and indirect energy types (Figure 1).

Direct-Energy Types of Equipment

A-C Single-Phase Welding Machines

The fundamental design of the resistance-welding machine, particularly the necessary secondary configuration for work clearance, makes the reactive component of current relatively high, giving the machine a high kilovolt-ampere demand and a low power factor. Since the welding machine is in reality a transformer with a low-impedance load, the current from the line contains two components, one in phase with the voltage and one lagging. Thus the welder is made up of a large reactive kilovolt-ampere component and a resistive-kilowatt component. The kilowatt component supplies the necessary power to make the weld and also the losses of the machine. The application of heavy-duty welding machines, with large and intermittent demands produced on the supply line, may create a cyclic flicker that becomes a nuisance to the power server and users of this equipment with the result that the utility engineer is tempted to discourage this type of a load, or to force some other solution to the problem. The sudden and repeated flow of current to the welder through the impedance of the supply system causes an intermittent voltage drop, which if great enough in magnitude, may cause objectionable flicker of the lamps of the lighting system fed from the same portion of the system. Two types of lamp flicker may be created by resistance-welding machines; a noncyclic flicker from single-impulse machines such as spot, butt, flash or projection welders, and a cyclic flicker from seam welders or spot and projection welders equipped with pulsation control.

Acceptable Voltage Drop

If the lamp voltage drops more than two volts out of 115 volts, the noncyclic flicker is usually objectionable to the lighting customers.

If the frequency of current flow exceeds one impulse a second, this cyclic flicker becomes evident and may prove objectionable. Here the borderline of objec-

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C. E. Smith is chief engineer, Taylor-Winfield Corporation, Warren, Ohio.

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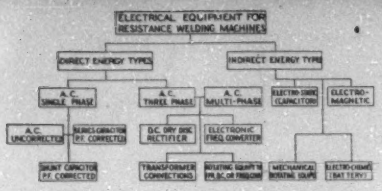


Figure 1. Direct and indirect energy types of equipment

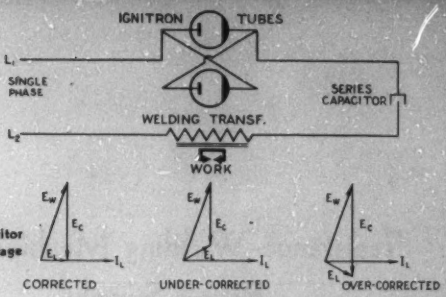


Figure 3. Series-capacitor circuit and vector voltage relations

I_L = line current
E_L = line voltage
E_W = welder primary voltage
E_C = capacitor voltage

tion or irritation follows a typical V curve, as shown in Figure 2, dropping to a minimum of about 1/2 volt at the worst frequency of seven or eight impulses per second.

When the economics of the final solution of installation of a welding machine in regards to the utility problem dictate the correction of the welding machine load itself, there are various ways of attempting to lower the kilovolt-ampere demand of that particular welding machine.

Series-Capacitor Power Factor Correction Equipment

Series-capacitor power-factor correction applications have been numerous and very successful, and with the present engineering information available for calculation of the necessary capacitors to correct a given welding job, it makes it a rather simple problem. Besides being expensive as a correction device, the series capacitors have definite disadvantages in that a certain turns ratio of the transformer must be as selected and adhered to, making it more or less applicable to one job at the same optimum conditions, unless the welding manufacturer is informed of the range of work which the machine is expected to do. In some instances, machines have been corrected with series capacitors which will correct the kilovolt-ampere and the power factor during the welding of aluminum, and by means of selector switches inserted to change the turns ratio of the welding transformer, make it possible to weld mild steel, and in some instances, stainless steel that requires a wide range of secondary current. Figure 3 shows an elementary circuit and the voltage vectors of a corrected, an under-corrected, and an overcorrected series capacitor system.

One of the arguments used against series capacitors has been that the machine must remain at approximately the same secondary configuration, and that if the throat areas change, it is necessary that an expert be available to revise the tuning or determine the correction of the circuit to bring it back to approximate unity power factor. However, if the welder machine is so designed with a maximum of throat configuration necessary for the job, and consideration given to the future applications of the job that the customer expects to weld in the future, the series-capacitor equipment becomes in some instances less expensive with a large throat machine than a small throat machine. Therefore, the maximum demand has been correct, and as the power company can serve such a demand, there is no reason why the secondary configuration should be changed.

The series-capacitor power-factor device at this time is the only device known to be economical and practical for achieving a unity power-factor load.

Another reason that may increase the cost of the series-capacitor power-factor correction-equipment installation is the cost of the control panel. This panel may be of the 2,300-volt variety, or out of the class of the standard 600-volt a-c controls. Another alternative is to make use of an autotransformer to reduce the input line voltage to a low value wherein the capacitor voltage plus the line voltage does not rise to over 600 volts, so that the ignitron contactor and nonsynchronous type of equipment can be used along with the standard 440 welding transformer primary. Figure 4 shows the kilovolt-

Figure 2. Objectionable limits of light flicker caused by welder loads

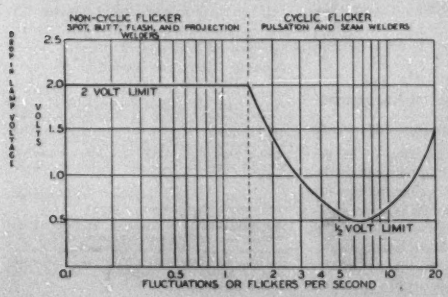
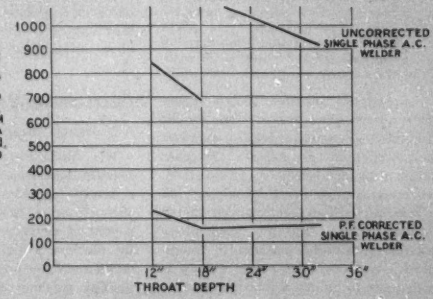


Figure 4. Kilovolts-amperes demand, uncorrected, single-phase alternating current versus power factor correction

Size Three Resistance Welder Manufacturer's Association (R.W.M.A.) press welder



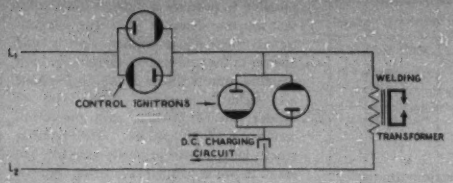


Figure 5. Shunt capacitor power factor correction circuit

ampere demands of a corrected and an uncorrected machine producing the same secondary currents.

Shunt-Capacitor Power-Factor Correction Equipment

Shunt capacitors have been used very successfully with many more or less constant load applications for many years, and it is a type of power-factor correction with which the utility engineer and most electrical engineers are familiar. Since the load in a resistance-welding machine is of a highly intermittent nature, the problem of power-factor correction with shunt capacitors is unusual as compared with ordinary power factor correction problems. The first thought that comes into the engineer's mind for correction of a resistance-welding machine is by the use of shunt capacitors across the primary of the welding transformer. The operational duty cycle of welding machines is quite low with the off-time periods allowing the shunt capacitors to discharge through the primary of the welding transformer. Due to the dead short-circuit characteristics of the capacitors, the inrush of charging current prohibits this type of correction.

It is possible to eliminate the charging-current inrush to the shunt capacitors by precharging them to the correct voltage and connecting them in parallel with the welder primary synchronously and simultaneously with the closure of the primary circuit to the power-supply feeder. This circuit is shown in Figure 5. It requires a standard synchroncus control in series with the welder load along with auxiliary devices for synchronous closing of the shunt-capacitor current and the precharging circuit. One pair of tubes permits the precharge of the capacitor by disconnecting it from the welder primary, and the other pair of power tubes acts as a synchronous switch on the supply circuit to control the welder in the conventional manner. This arrangement requires two sets of power tubes and a precharging circuit which are generally expensive in relation to series-capacitor circuits.

Another application of shunt capacitors can be as shown in Figure 6 wherein the shunt capacitors are connected permanently across the supply feeders as close to the welding machine input leads as possible. The disadvantage of this type of correction is that during the idle or no-current flow period of the welding machine, the device will attempt to overcorrect the supply line according to the auxiliary load on these lines. If however, a device is applied to the shunt capacitors that will prevent overcorrection or overvoltage of power-supply lines, the application may have considerable merit. A thorough study must be made before application of such devices to a large number or group of welding machines with consideration given to the customer's supply-line impedances. A suitable reactor for cancelling out the fifth and seventh harmonics that are developed due to the transformer and capacitor type of circuit is worth considering since the fifth and seventh harmonics in this type of a circuit can create considerable power losses. With the elimination of the fifth and seventh harmonics, more correction kilovolt-amperes can be obtained from a standard-kilovolt-ampere capacitor unit.

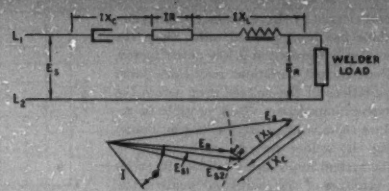


Figure 7. Vector diagram showing improvement of voltage

E_S—Line supply voltage
E_R—Welder load primary voltage
E_{S1}—Readjusted line voltage required after compensation to give same E_R
E_{S2}—Supply voltage required after compensation for resistance and reactance drops to maintain same E_R

To tune the circuit to approximately unity power factor with any shunt capacitor method becomes quite expensive and the installation is generally corrected to approximately 85 per cent power factor and thus falls within economical ranges. If the power factor of the load is 50 per cent or higher, the shunt-capacitor application may be less expensive than the series-capacitor application. However, if the power factor is 50 per cent or lower, the series-capacitor application may be less expensive.

Series Capacitors in the Supply Line

Installation of series capacitors for power-factor correction of resistance-welding machine in the welder circuit has been discussed. The voltage regulation problem because of the high and intermittent variations of the amount of cur-

Figure 6. Shunt capacitor power factor correction of single phase a-c machine

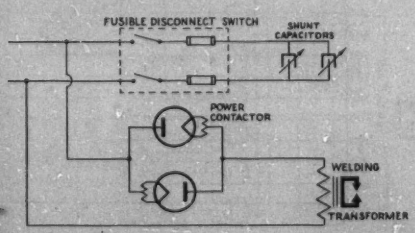
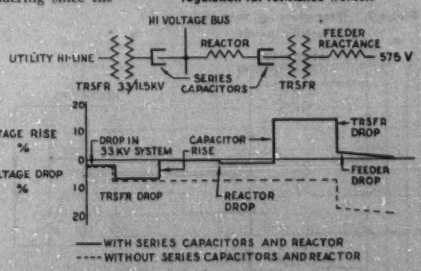


Figure 8. Voltage chart illustrating voltage regulation for resistance welders



rent created by the resistance-welding machine has been pointed out. Since the welder is operated at a low power factor, these current variations may cause large variations in the voltage drop across the system impedance. It is possible to reduce these voltage drops either by reducing the welder current, or by reducing the system impedances. With the series capacitors at the individual welders, the capacitor reduces the line current by improving the welder power factor. As a result, the voltage drop across the system reactances are reduced and the voltage regulation is improved. This scheme is particularly desirable where the welder load is made up of a few large welders and the diversity factor is low.

If the welder load is made up of a large group of welders and the diversity factor is high, it is often economically advantageous to install series capacitors in the supply line, and thereby take advantage of the higher diversity factor. Figure 7 shows the capacitors installed in this manner for improving voltage regulation by neutralizing the system reactances. The vector diagram illustrates the improvement of voltage regulation by this system. By proper capacitor selection, it is possible to hold the voltage regulation within very narrow limits.

Generally the series capacitors are installed in the line immediately ahead of each load center unit. The bus voltage is not constant and varies because of fluctuations in load current which causes variation of the voltage drop across the system reactances between the power source and high-voltage bus (the system reactance is made up of two main components, the utility-system reactance and the reactance of the transformer between the utility's high line and the high-voltage bus). The utility-system reactance is practically constant, but the transformer reactance will depend upon the number of transformers in service. It is not desirable to install any capacitors in the welder feeders supplying a group of welders because of the variations in reactance which

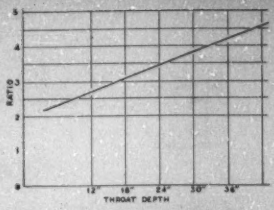


Figure 10. Ratio of single-phase kilovolt-amperes to 3-phase kilovolt-amperes (metallic rectifier) on size three R.W.M.A. press welder

they would be called upon to cancel, and because it is desirable to maintain the high-voltage bus voltage essentially constant to the benefit of other connected loads. Hence, the capacitors should be installed in series with each step-down transformer between the distribution transformer and the high-voltage bus. As additional transformers are added for additional service, an additional capacitor unit can be provided.

Even though every care is taken to select the proper capacitance values, it is not possible to calculate the system reactance exactly. For this reason, provision is made for tuning the capacitors in the field, simply by adding or removing units. By tuning the capacitors, system reactance can be matched very closely.

The over-all voltage regulation is illustrated by the voltage regulation chart, Figure 8. Without series capacitors, the welder voltage drop will be approximately 20 per cent under peak-load conditions. With the series capacitors installed, the small voltage drop of the welders will be due almost entirely to the system resistance.

Metallic Rectifier (Magnesium Copper-Sulphide Type)

A 3-phase metallic rectifier capable of delivering direct current into the secondary of the welding machine makes it possible to produce a 3-phase lower kilovolt-ampere demand than the conventional single-phase welder and is one answer to the problem of lowering the high demand usually found in the single-phase a-c welding machine. This type of welder can be a seam, spot projection welder or flash welder. The rectifier stacks have an exceptionally long life, are small in size as compared to other types of electrical components and are of rugged construction. These rectifiers are very suitable to a welder load, being capable of supplying heavy short-time current overloads, with power consumption at no-load practically nil. For the most economical transformer design and most satisfactory rectified wave form for the particular d-c output voltage and current required in a resistance-welding machine, the rectifier circuit should be 3-phase full wave. (Figure 9).

The metallic rectifier type of machine uses rectifier transformers instead of a welding transformer and with the 3-phase full-wave circuit, is much more efficient and economical in comparison with the 3-phase half-wave circuit. The kilovolt-ampere demand will generally be less to make a given weld in comparison to alternating current, or any frequency conversion system and since the welding current is direct current, the effect of the secondary configuration is less noticeable as to reactive electrical losses. Figures 10 and 11 show a comparison of the 3-phase kilovolt-ampere demand of a metallic rectifier machine with the single-phase kilovolt-ampere demand of an a-c machine producing the same secondary currents. The kilovolt-ampere figures for the 3-phase machine are the line current times the line voltage times the square root of three.

Due to the values of resistance and inductance usually found in a welding

Figure 9. Three-phase full wave circuit of d-c metallic rectifier

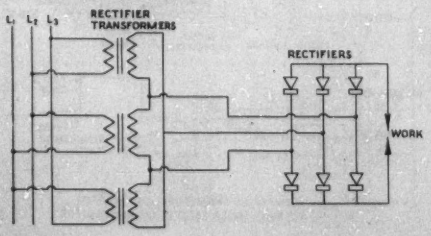
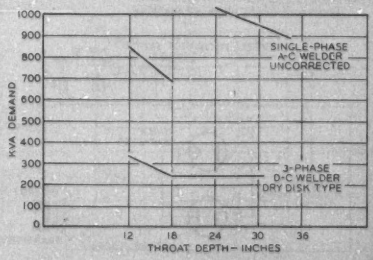


Figure 11. Kilovolts-amperes demand single-phase alternating-current and 3-phase direct-current versus throat depth

Size three R.W.M.A. press welder



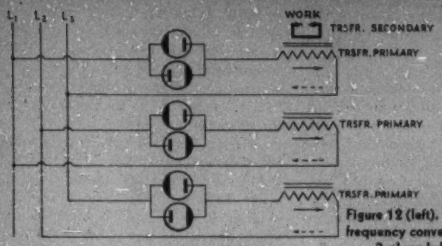


Figure 12 (left). Electronic frequency converter system, 3-phase half wave

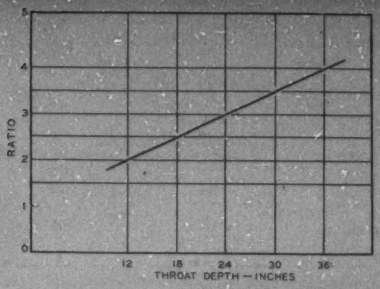


Figure 14. Ratio of single-phase kilovolt-amperes to 3-phase kilovolt-amperes (frequency conversion) on size three R.W.M.A. press welder

machine, the rate of rise of current in a metallic rectifier machine is rather slow, making it particularly desirable for projection and upset-butt welding. The current actually rises faster than the calculated static values of R and L would ordinarily permit, due to the fact that the rectifier resistance changes during current rise. At the start of current rise, the applied voltage makes the current start in such a manner as though the apparent maximum current would be about twice the actual maximum. Approximately eight milli-seconds after start of current flow, the voltage decreases to a low constant value reducing the overshooting tendency of the voltage and current rise.

One factor requiring consideration in the application of metallic rectifiers in welding machines is the proper analysis of the efficiency. A study of the impedance curve and the efficiency measurements will show that the efficiencies are lowest at low currents and with low-impedance loads. With a given load impedance, the efficiency improves as the current increases. With a given current, the efficiency will improve as the load impedance increases up to a point where the load impedance equals the rectifier impedance. In resistance-welder applications, there is no object in attempting to operate the rectifiers at a maximum efficiency as long as the impedance adjustment must be in the load circuit, not in the rectifiers themselves.

Since the rectifier units are essentially resistance devices, the power factor will be between 90 and 100 per cent, and the primary current wave is without the discontinuity which is present when using rectifier tubes. The distortion of the primary current wave is negligible. With the use of the 3-phase full wave circuit, it can be stated that the primary current will be balanced almost perfectly.

Electronic Frequency-Converter System

The use of electronic frequency-converter systems to reduce the frequency of the secondary current to a low value, thus reducing the inductive reactance of the secondary loop circuit has merit as to the reduction of the kilovolt-ampere demand. At the present time, there are two types of rectifier circuits being used by one resistance-welding manufacturer and these circuits are shown in Figures 12 and 13. By eliminating the rectifier transformer (shown in Figure 13), Figure 12, and making use of a 3-phase welding transformer with the ignitron tubes properly connected for 3-phase half-wave rectification, this type of converter becomes practical as a correction device. The welding transformer must be designed for the very-low frequencies (5 to 10 cycles), and therefore, much larger than a 60-cycle transformer of comparable rating. The electronic frequency-changing circuits draw almost balanced 3-phase power from the line and have a low-frequency single-phase output. The power demand is not perfectly balanced on the three phases due to the nonsinusoidal nature of the current wave. Double advantages are secured from such an electronic circuit because the power factor increases when the load is operated at a low frequency, and the ratio of 3-phase kilovolt-ampere demand to single-phase kilovolt-ampere is approximately four-to-one in the deep-throat machines. Commutation faults, however, must be given consideration with the introduction of a low frequency system since during one or more cycles (on supply frequency basis) the entire load is single phase, resulting in a momentary increase in voltage drop of two lines. This momentary drop may have a very small value when read on an rms meter, but it may

Figure 13. Electronic frequency converter 3-phase double Y parallel with center tapped welding transformer

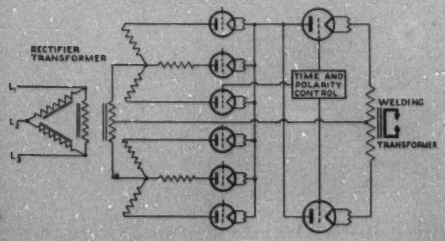


Figure 15. Kilovolt-amperes demand, single-phase alternating current and 3-phase alternating (frequency conversion) versus throat depth

Size three R.W.M.A. press welder

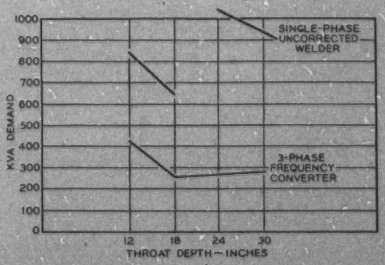


Table I

Case	Number of Welders	Type of Welder	Assumed Voltage Drop*
1A	1	3-phase metallic rectifier, or electronic converter type of welder	1 per cent
1B	3	3-phase metallic rectifier, or electronic converter type of welder	
2A	1	1-phase, series-capacitor power factor corrected**	1 per cent
2B	3	1-phase, series-capacitor power factor corrected**	
3A	1	1-phase, uncorrected alternating current (Correction factor 3 to 1)**	3 per cent
3B	3	1-phase, uncorrected alternating current**	

* Same supply system in all cases.

** Phase control, if any, set at 100 per cent heat.

still cause objectionable lightflicker. The only drop that must be considered in analyzing the possibility of lightflicker is the drop on the load-phase line. Figures 14 and 15 show the comparison of the 3-phase kilovolt-ampere demand to single-phase kilovolt-ampere demand producing the same secondary currents.

One of the improvements that can be made to the converter system is to close up the zero-current gaps during the transfer, or change in polarity of secondary current. With the introduction of zero-current gaps during current flow, the impulses per second or flickers per second may fall in a range of approximately nine per second, or within the 1/2-volt limit, commonly termed as cyclic-flicker range similar to pulsation or seam welding on and off timing periods.

The frequency-converter system is somewhat likened to the metallic rectifier in that, since it is a high-power factor device, (approximately 85 per cent) it is resistance limited and generally no-current compensation is required or any correction necessary for the introduction of magnetic material in the throat of the welder machine.

Rotating Equipment

To offset an inadequate power supply, motor-generator sets have been used, but due to their original cost, maintenance cost and their short-time transient regulation, they have not usually proved satisfactory. To offset the first two disadvantages, a large motor-generator set is generally employed to handle a group of welders, but the transient reactance with simultaneous operation of the machines makes this system generally undesirable.

A motor-generator set, with 3-phase input and single-phase output of commercial frequency or low frequency, may have satisfactory application to a single machine, that is, one motor-generator set per machine.

The possibilities of designing a motor-generator set with a generated direct current may have possibilities in the application of resistance-welding machines. The original cost of such equipment and the problems of commutating, collecting and making and breaking heavy secondary currents may be objectionable. Since many welder loads have operating duty cycles in the order of 5 to 10 per cent, the no-load losses of the motor-generator set during the remaining 90 to 95 per cent of the time may affect over-all efficiency.

The use of a fly wheel with a motor-generator set may give it some characteristics of an indirect-energy (or stored-energy) welder.

Transformation by Means of Transformers

The most common transformation is the connection of a single-phase load to a 3-phase line, and many attempts have been made to do this to obtain a balanced 3-phase load. The only method that is known at the present time is to divide the load into three equal parts and connect one part to each phase. This method is desirable and often practical. There are, however, many different methods of obtaining a single-phase voltage from a 3-phase system by using transformers and which result in varying distributions of the current in the 3-phase system. Of all these connections, the "Scott," Figure 16, is the most suitable for usage in conjunction with welding machines. The data taken from one installation showed that if a straight single-phase connection resulted in a 4 per cent line in voltage drop, during the weld on the one leg of a 3-phase line to which the welder was connected, 2 per cent on one of the other legs and 1/2 per cent on the third leg, the use of an auxiliary "Scott" transformer and connector would result in approximately 2 per cent voltage drop on two legs and approximately zero drop on the third leg. It can be seen from this that the voltage drop on the line to which the welder was connected was cut in half, a considerable improvement.

This "Scott" connection cannot result in a balanced 3-phase load. The single-phase voltage applied to the welder from the transformer is 0.866 times the standard line voltage; to obtain proper output, therefore, the welding transformer must be especially wound to operate on 86.6 per cent of the line voltage, and the welding-power control equipment of the synchronous type must be operated from the same reduced voltage. One of the schemes, shown in Figure 16, is the most commonly suggested and the values given with the diagrams are those based on a one-to-one ratio of transformation. Since the voltage at the primary of the welding transformer is reduced by 0.866, the primary current will be increased by 1.15, if the machine is to produce the same secondary currents.

The apparatus economy is the ratio of the maximum kilovolt-amperes which the bank can deliver to the total kilovolt-ampere of apparatus making up the bank. Tables I and II give the characteristics of these connections and show that they are generally inferior to a single-phase transformation. By straight single-

Figure 16 (left) and Figure 17 (right). Transformation by means of center tap transformer

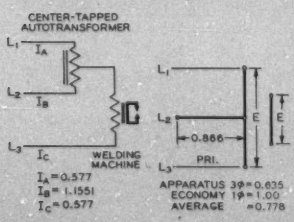
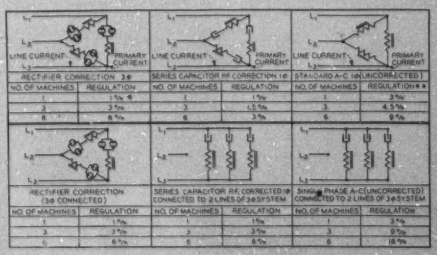
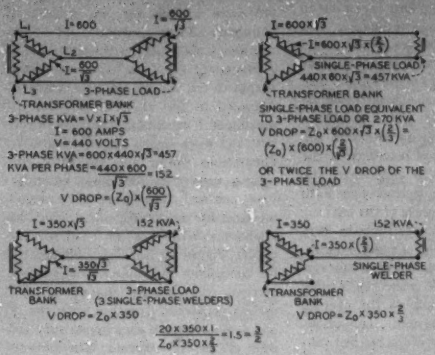


Figure 18 (left). Different types of correction



* Assume one per cent regulation on the same system and to be reasonably typical for machines of each type to do the same welding job

** Assume correction factor of 3-to-1



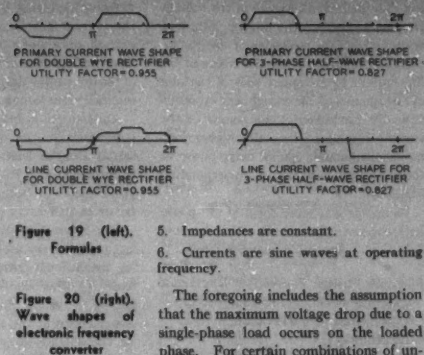


Figure 19 (left). Formulas

Figure 20 (right). Wave shapes of electronic frequency converter

phase connection of one transformer between two wires of a 3-phase system, the total value of power delivered is the same in all cases. The only advantage that the "Scott" scheme has will be noted that the delta-connected generator has a maximum current of 0.577 as against 0.667 of the straight single phase. To offset this, however, this scheme required two transformers possessing greater total capacity and also impose upon one line a greater maximum current.

Regulation Characteristics of 3-Phase and Single-Phase Machines on 3-Phase Lines

One interesting feature of the regulation characteristics of 3-phase machines versus single-phase machines on a 3-phase line is shown in Figure 18. The regulation of a 3-phase dry-disk rectifier machine, or a 3-phase electronic-converter machine under an assumed regulation of one per cent on the system (with these assumed voltage drops caused by one welder of each type, or agreed to be reasonably typical for machines of each type to do the same welding job on the same power system) will produce three per cent regulation with three machines of the 3-phase type, but only $1^1/_2$ percent regulation with three series-capacitor power factor corrected machines distributed around the three legs of the 3-phase system. Figure 18 also includes the regulation of the uncorrected single-phase machine distributed around the 3-phase system with the assumption that the correction factor is three-to-one.

A given kilovolt-ampere of a 3-phase load causes one-half as much as percentage rms voltage drop as the same kilovolt-ampere of a single-phase load. This is proved in Figure 19.

In the illustrated cases using 1-phase welders in Figures 18 and 19, the maximum drop created by three welders is only $1^1/_2$ times the drop due to one welder. This may be explained also by the use of the well-known rule that a given kilovolt-ampere of 3-phase (sine wave) load causes half as much maximum rms voltage drop as the same kilovolt-ampere of a single-phase load. Hence, although the kilovolt-ampere is increased by three, distribution of the single-phase welders on the three phases makes this load three phase. Therefore, the drop is three over two of its value for a single 1-phase welder as shown in Figure 19.

Table I illustrates the voltage drops created by one welder, and Table II illustrates the voltage drops created by three welders.

Table II has the following conditions assumed:

1. Welder types are as given by Table I.
2. Voltage drop for any one welder is as given by Table I.
3. Single-phase welders placed each on a different phase (line to line).
4. Same supply system in all cases.
5. Voltage measurements taken in the same place in all cases.
6. No Y-delta transformation between load and voltage measurement point.

Assumptions for Voltage Drop Rule*

1. Positive and negative phase sequence and impedances of supply system are equal.
2. Voltage drop is a small fraction of the total voltage.
3. The maximum voltage drop, due to the 1-phase load, occurs on the loaded (line to line) phase.
4. Same supply system at all times.
5. Impedances are constant.
6. Currents are sine waves at operating frequency.

The foregoing includes the assumption that the maximum voltage drop due to a single-phase load occurs on the loaded phase. For certain combinations of uncorrected welder power factor and supply system, X over R ratios, the maximum-voltage drop due to a series-capacitor corrected welder may occur on a different phase. However, in such instances, the drop per case $2B$ (three welders) will be less than as given by the rule. In these instances, the voltage drop caused by three such welders (at 100 per cent phase-control setting) may even be less than for one, if the system X over R ratio is sufficiently high.

Primary Wave Shapes of Rectifier Circuits (Metallic Rectifier and Electronic Frequency Converter)

A polyphase system or circuit is said to be perfectly balanced when conditions in all phases are the same, that is, when the voltages across all the phases, the currents flowing in all the phases, and the power factor of all the phases are the same. When any one of these quantities is not the same in all phases, the system is said to be unbalanced.

An investigation of rectifier circuits is primarily a study of wave forms. Unlike rotating apparatus, a rectifier stores no energy within itself, and there is a constant connection between voltages and currents on the a-c side and voltage and currents on the d-c side.

The voltage at the primary terminals of a rectifier is sinusoidal while the current is nonsinusoidal. It may be resolved into a number of sinusoidal components consist-

Table II. Voltage Drops Created by Three Welders

Case	Maximum Voltage Drop, Per Cent
1B	3
2B	$1^1/_2$
3B	$4^1/_2$

* A given kva of a 3-phase load causes one-half as much percentage rms-voltage drop as the same kilovolt-amperes of a single-phase load.

ing of a fundamental wave of the voltage frequency and of the higher harmonic waves which are multiples of the fundamental frequency. The rms value of the line current is the square root of the sum of the squares of the component currents. Only the fundamental component of the current carries power; the higher harmonic components cannot carry power as there are no voltages of corresponding frequency.

Figure 20 is a wave form of a 3-phase half-wave rectifier circuit and a 3-phase double-wye paralleled rectifier circuit.

Primary Power Factor of Rectifier Circuits (Electronic Frequency Converter and Metallic Rectifier)

The power factor is similar to any rectifier using a 3-phase half-wave circuit, or a 3-phase double-wye paralleled circuit.

Under ideal conditions, the power factor is the square root of two times the utility factor, or

$$\text{Power factor} = \frac{2\sqrt{P}}{\pi} \sin \frac{\pi}{P}$$

P = number of phases

The double-wye circuit, Figure 13, will give a higher utility factor than the 3-phase half-wave circuit (Figure 20). It is interesting to note that in the double-wye circuit or the 3-phase full-wave circuit (where mentioned and calculated), windings will be almost as effectively utilized as though they carried sine-wave current.

The ratio of watts to volt amperes is a somewhat complicated function as it consists partly of an out-of-phase component and partly of a component due to the distortion of the wave form. Even on a resistive load there is a form factor due to the current not being continuous. The current ceases to flow during the period that the transformer voltage is below that of the arc-drop voltage of the tubes. The rectified wave form is not a true sine wave.

An inductive load (there is always inductance in the circuit either in the supply line, or leakage reactance at the transformer and leads) also affects the power factor by both decreasing the form factor as well as the angle of the lag.

It is interesting to note that the form factor has a very useful effect on improving the over-all power factor of the system. In other words, connecting a rectifier to a system improves the over-all power factor of the system. In particular, the wattless input of the rectifier cannot be added algebraically to the wattless input of reactive equipment for all of the harmonic components but must be added at right angles. The true power adds directly to the system, while the fictitious power being largely harmonics, makes no appreciable addition to the reactive power already carried.

Indirect Energy Types of Machines

The electrostatic (capacitor), the electromagnetic (reactor), the mechanical (rotating equipment with flywheel) and the electrochemical (battery) systems are systems where the energy to make a weld is stored during the no-weld period and released during the weld period. The energy control devices are generally 3-phase input units.

The kilovolt-ampere demands of these systems are shown in Figure 21 in comparison to a single-phase a-c machine to do an identical welding job (aluminum).

Although these devices limit the kilovolt-ampere demand on the supply lines, application of the machines must be given consideration as to the ability of welding certain materials. The electrostatic and electromagnetic machines with their rapid rate of current rise and type of wave shape are more suitable for the welding of aluminum and magnesium alloys in the commonly used range of thicknesses.

Figure 22 gives the secondary wave shapes of the various types of machines discussed in this article. The time to maximum, T maximum, rate of rise of current and wave shapes are representative illustrations only. The electronic frequency converter, the motor-generator type, and metallic rectifier wave shapes are fixed by the total R and L, and the regulation of the systems. The battery welder, Figure 23, has a variable resistor for controlling the value of the welding current, and pressure changes on the carbon contactor (together with the R and L of the rest of the circuit) vary the rate of rise of current. The battery welder has a wave shape similar to that of the metallic rectifier.

In addition to R and L of the circuit, the electromagnetic system, Figure 24, in some instances has alloy blocks inserted in the secondary circuit for changing the wave shape.

An important feature of the capacitor stored energy machine, Figure 25, is its ability to deliver a current wave shape of almost any desired rate of rise through adjustments of the welding transformer turns ratio, the amount of capacitance variations of the capacitor voltage give close control over the energy to make the weld.

Effect of Secondary Wave Shapes in Respect to Welding

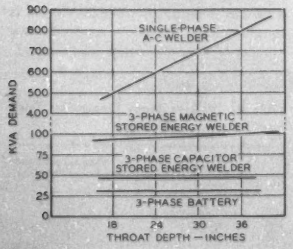
Frequency converter, metallic rectifier machine, motor-generator type, and battery welder:

Advantages

1. In projection welding or upset welding nonferrous alloys, the current rise is slow and peak current in relation to rms is low enough to prevent blowout of material at start of weld.
2. In spot or seam welding heavy sections

Figure 21. Kilovolt-amperes demand of single-phase machine, magnetic-stored energy machine, capacitor stored energy machine, and battery machine

Size three spot welder. Machines equivalent in ability to weld same material at approximately the same rate of operation



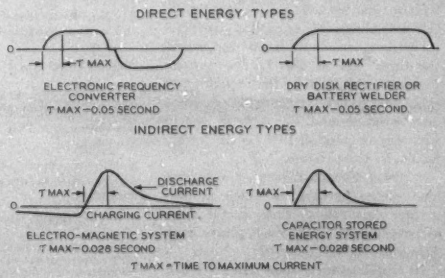


Figure 22 (right). Secondary current wave shapes of direct and indirect systems

T Maximum—time to maximum current

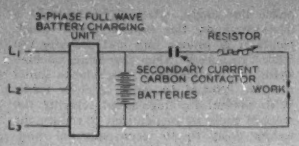


Figure 23. Electrical circuit of battery welder

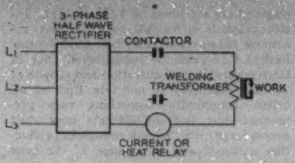


Figure 24. Electromagnetic stored energy systems

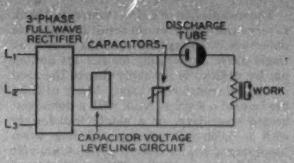


Figure 25. Elementary diagram of electrostatic stored-energy system

of aluminum, the rate of rise of current is slow enough to permit electrodes to follow and heat before electrode burning occurs. This is a decided advantage over 60-cycle a-c welding.

3. It is possible to make the weld, in heavy aluminum sections, over a longer period of time than on the electromagnetic or capacitor stored-energy systems where the weld must be made in the region of peak current. With the travel of heat in the material and the nugget forced to grow fast, spitting sometimes occurs before the nugget has fully grown when welding with stored energy.

4. The slow rate of rise of current will produce more consistent joints in aluminum with inconsistent cleaning or commercial uncleaned aluminum than in comparison to a-c or stored energy of the capacitor or magnetic type.

5. There is no need for current compensation due to magnetic material changing its position in the throat of the machine.

6. Double-pressure timing need not be so accurate.

Disadvantages

1. Low frequency will be detrimental in shunt currents in adjacent welds. Since the impedance between spots changes with the frequency, very careful consideration should be given to shunt currents created by adjacent spots.

2. In comparison to the metallic, battery machine or motor-generator set (alternating current to direct current) and their continuous current conduction in respect to the envelope wave shape of the frequency current converter and with zero current gaps during secondary current change in polarity, the weld time will be longer or more heat is required. For every cycle of zero current gap, it will require a cycle of full current to regain temperature in the weld equivalent to the temperature at the end of the preceding weld. This leaves the balance to steady-state current or maximum current at the end of each wave that actually raises the temprature of the weld to welding temperature.

3. The machines, being high power factor devices, are resistance conscious. Therefore, more care must be taken in concentrating resistance in the work.

4. In welding of thin aluminum, it requires more secondary current than a-c welding. The nugget can grow faster than the energy can be placed in the joint.

Figure 22 shows some typical wave shapes produced by these various types of welding machines.

Summary

The final selection of welding equipment must have the following considerations:

1. The ability of the equipment to weld the specified material.

2. The cost of the equipment.

3. The voltage regulation imposed on the supply system by various types of machines.

4. The cost of reinforcement of the present supply system versus the cost of the machines and their correction devices.

5. The total of machines involved and their diversity factor.

6. The rate of production of one system versus another keeping in mind the limitations of each system and the number of machines involved.

The a-c single-phase machine is generally found the most economical if the power supply is adequate, if not, a complete study must be made of the total installation and the power system before recommendation of any certain type of machine and its correction device versus power-system reinforcement.

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Discussion

C. N. Clark (Duquesne Light Company, Pittsburgh, Pa.): When determining the proper type of power supply for various types of resistance welders, utility engineers often encounter certain limitations which would not apply if service were to be provided instead to an equivalent steady 3-phase load. With the wide variation of conditions at various locations on a power system, individual study of each welder load is necessary in order to provide the most economical service, and at the same time make possible the satisfactory operation of the welder without disturbing service to the many thousands of light and power customers in the same general neighborhood.

It is noted that the author divides voltage fluctuations with accompanying lamp flicker into two classes:

1. Noncyclic, slower than once per second with a 2.0-volt tolerance.

2. Cyclic, faster than one and one-half times per second with tolerances as low as 0.5-volt in 115-volt terms.

Our experience, based upon a large number of observations by several independent groups, indicates that the border line of irritation follows a rather smooth curve with no sudden break at any particular frequency. The curves most frequently used by utility companies show a permissible 2.0-volt fluctuation at a frequency around one per minute, decreasing gradually to values of 0.4 to 0.5 volt at a frequency of approximately 6 per second. These values are considerably under those shown by the author in the curve of Figure 2.

The use of series and shunt capacitors for power factor correction and reduction if instantaneous demands for resistance welders should be encouraged, since in many cases the additional cost for power factor correction is much less than the extra investment necessary to reinforce the power system and reduce the voltage fluctuations which would otherwise result from the use of an uncorrected welder capable of welding the same material.

The installation of a Scott connected bank to serve the welder would result in a sizeable reduction in voltage fluctuations caused by single-phase a-c welding loads. Its limited use by utilities is probably due to the need for special transformer taps. Many utilities use instead a 3-phase bank with a delta connected secondary, or a single transformer with a 4,000-volt primary winding when supplying a welder from a 2,300/4,000-volt 4-wire 3-phase system. The regulation of the primary circuit by either of these methods may be from $33^1/_3$ to $66^2/_3$ per cent less than the regulation obtained by serving the same welding load from a single phase 2,300-volt transformer connected between the phase wire and neutral.

This variation depends upon the relative values of impedances for the substation equipment and the conductors of the primary circuit for each location. The voltage regulation due to the use of various types of resistance welders is also affected by the X-R ratios of the power system and the X-R ratio of the welder.

On a 2,300/4,000-volt 4-wire 3-phase system, a balanced 3-phase load would cause one-sixth as much voltage regulation in the primary circuit conductors as an equivalent single-phase load supplied from a 2,300-volt single-phase transformer connected to the phase wire and neutral of the same circuit. This ratio offers a tremendous advantage for any balanced 3-phase welding load, provided an accompanying cyclic flicker can be avoided.

The description of the characteristics and basic circuits for the various types of 3-phase welders should be very useful to utility engineers, and should help them in their work of providing adequate welder service at a minimum cost. The consideration given by the author to the effect of welder operations on the power supply system is greatly appreciated, as too much emphasis cannot be placed on this phase of the problem.

E. M. Callender (The Budd Company, Philadelphia, Pa.): This paper is a résumé of many types of resistance welding equipment applicable to the widely varying power supply conditions encountered in factories and locations employing the process. The author is to be congratulated on grouping these various types of equipment and for enlarging upon their characteristics. The paper covers in modest detail the power supply problem. It also deals more specifically with the required connection of each type machine.

Consideration is hereby given in connection with the introductory statement concerning the use of single phase a-c welding machines whose supply is taken around a 3-phase system. Loading a group of single-phase welders fairly evenly around a 3-phase plant feeder may achieve:

1. Economy in a long bus feeder.
2. An improvement in the regulation of supply feeder and hence improvement in weld consistency.
3. More effective use of existing maximum available supply of kilovolt-amperes.

This is in comparison with use of single-phase feed on the same application. It is likewise verified in the paper by comparative regulation values under regulation of single-phase machines on 3-phase lines. Incidentally, it is feasible to install low reactance balanced or fairly balanced 3-phase feeders. It must be remembered that in large plants the concentration and growth of welding machines is not like that of an urban community—something very permanent—but the installations may shift over a large area through change in production methods and by change in the type work.

It is difficult to judge from technical papers the relative extent of various welding problems on power supply. Even so, every installation study will naturally divide itself into two parts.

1. Power company supply line that must satisfy both utility and customer to the extent of the compromise necessary.
2. Adequate plant distribution feed and welding equipment to handle the maximum capacity of resistance welding with recommended regulation that will provide consistent welding conditions under diversity loads.

Obviously, in power scarce areas the choice of (2) must be made with such power demand economy that (1) can be satisfied based on the economics involved. By the same token the customer may not be allowed to obtain increased weld loads from the power company at a later date. There is a three man team of utility, equipment manufacturer, and customer who must work closely together for the coherence of a well co-ordinated installation. Yet, much of the responsibility falls on the shoulders of the welding engineer and his company for proper selection of equipment.

In an art that dates far back most of the advances in resistance welding have been made in slightly over a decade through the introduction of electronic tubes. Early processes were modernized and many new equipment developments occurred through the advent of this precision power controlling device. Since then many fine technical contributions have been made covering advances and solutions to more numerous problems.

The welding engineer may find insufficiency in technical papers taken individually, since they may not provide the exact information required. He really has a need for engineering texts on the subject. Collectively, these welding papers have greater value, and the AIEE electric welding committee has possibly advanced the cause by compiling a bibliography covering major technical papers in various journals on equipment and power supplies written since 1939. This will be available in the near future.

There is a challenge for the introduction of engineering texts on the subject of power supply problems and equipments. Such books would have to be authoritative, very complete, and should contain a wealth of customer experience. Technical papers could form a nucleus for subject matter in some cases.

The author's summary concluded that the a-c single-phase machine is generally most economical where power supply is adequate. The writer wonders if any advantage might be taken by low production users having groups of two to five a-c single-phase welders (on a phase) in certain critical utility power line cases of small users wherein all units are relay interlocked to take their turn on the power line singly. Each call for a weld is memorized on the control board which places it in proper turn and provides the relay "go ahead" when preceding machine welds are completed. This scheme limits the line demand to single machine loads. With present-day high-speed relaying the method could be modernized to reduce the welder delay to an inconsequential minimum.

Whatever equipment is chosen in an application, it should retain flexibility as a keynote to its lifetime usage. Choice of welding equipment, aside from the load demand, ought to have as a consideration its general utility on future jobs that may vary considerably from the initial application.

Finally, one unnecessary line overload can be prevented with a-c electronic heat controlled welders by maintaining the welding transformer voltage tap so that small no-current gaps between waves will occur for any heavy welding setup. Large electrical phase back angles on heavy loads will increase the peak line current by as much as 25 to 50 per cent and add that much increased regulation problem.

W. K. Boice (General Electric Company Schenectady, N. Y.): The paper emphasized the desirability of a resistance welding load which is 3-phase and is balanced. The most important reason for desiring balance is that the drop in rms voltage, on a given 3-phase system, caused by a balanced load is essentially half that for a single-phase load of the same kilovolt-amperes and power factor. The other reasons listed in the paper are, we believe, seldom of comparable importance.

The paper points out that metallic rectifier welders have less harmonic content in the current wave than some frequency converter welders. It implies that converter welder loads are consequently less perfectly balanced. However, the degree of unbalance is independent of the percentage of harmonics in the input current wave, because the harmonics are essentially the same for all phases.[3]

The degree of unbalance is practically negligible for either a metallic rectifier welder, or a frequency converter welder.[1]

The voltage drop for either type of welder may be estimated with reasonable accuracy by neglecting harmonics, and by assuming the load to be perfectly balanced.[1]

When harmonics are neglected, the input current is a sine wave which lags somewhat behind the voltage wave. The angle of lag is caused principally by the presence of leakage reactance between transformer windings used for rectification or conversion. It may be determined by the welder designer by methods[2] employed in power rectifier practice, and should be considered in estimating voltage drops.

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Principles and Practices of Relaying in the United States

E. L. HARDER
MEMBER AIEE

W. E. MARTER
ASSOCIATE AIEE

Synopsis: The rapid growth in power system size and interconnection over the past 30 years has greatly increased the problem of isolating faults. Standards of service have increased, making necessary greater reliability, and higher speed of operation. Innumerable relaying devices and schemes have been developed to meet the more exacting requirements. Some have been designed for special situations and others apply to basic situations which occur on many systems. It is these latter which it is hoped to discuss in this paper.

THE AIEE through its relay committee has encouraged these developments through the dissemination of information in hundreds of technical papers, conferences and committee reports.* The relay committee maintains an up-to-date bibliography of these papers and reports, as well as of other published articles and books. The 1944 to 1946 section was issued in November, 1947.[1] We shall draw freely on the fundamental data contained in the reports and papers of the relay committee.

General Philosophy of Relaying

The system is divided into protective zones as shown in Figure 1, each having its protective relays for determining the existence of a fault in that zone and having circuit breakers for disconnecting that zone from the system. It is desirable to restrict the amount of system disconnected by a given fault; as for example to a single transformer, line section, machine, or bus section. However, economic considerations frequently limit the number of circuit breakers to those required for normal operation and some compromises result in the relay protection.

The relays operate usually from currents and voltages derived from current and potential transformers or potential devices. A station battery usually provides the circuit breaker trip current. Successful clearing depends on the condition of the battery, the continuity of the wiring and trip coil, and the proper mechanical and electrical operation of the circuit breaker as well as the closing of the relay trip contacts.

In event of failure of one of these elements, so that the fault in a given zone is not cleared by the first line of defense, relays and circuit breakers, some form of back-up protection is ordinarily provided to do the next best thing. This means, first of all, to clear the fault automatically, if at all possible, even though this requires disconnection of a considerable portion of the system. Once cleared, the system can generally be rather quickly restored, whereas if the fault hangs on, the line may be burned down, or apparatus damaged beyond repair, or the entire system may be shut down for an extended period. The measures taken to provide back-up protection vary widely depending on the value and importance of the installation and the consequence of failure. These will be discussed in a separate section.

Some utilities in measuring the performance of transmission line relay protection analyze all relay trip operations as shown in Table I. The numbers shown are typical of a system operating 3,000 miles of 110-kv line.

This is only an analysis of faults for which the relay tripped or should have tripped. For each of these there were several cases where the relays should not have tripped, and did not. Thus the total number of discriminations made by the relays is possibly five to ten times as great as the trippings. The percentage failures are correspondingly less on this larger basis.

However, Table I has been presented at this point to bring out the following factors that enter into a highly successful protective relay system:

1. Good equipment, relays and instrument transformers.
2. A system design that can be protected and correct application of relays to provide the possible protection.
3. Good maintenance primarily to assure that all the accessories are operative.

The correct but undesired trippings are cases where the relays have done what should be expected from their characteristic curves and settings and the fault conditions involved. There may have been system changes since their application, or incorrect initial application, or application with foreknowledge that certain conditions would unavoidably operate the relays, but this was necessary to secure tripping in other desired cases.

It is important to bear in mind that simple standard system design plans can be better protected. Distance measuring and carrier or pilot-wire types of relaying are much less subject to disqualification by system changes than are over current types.

Wrong tripping and tripping failures, together with all causes of failure to clear faults, have been broken down further in AIEE conference reports and found to stem largely from human errors, such as leaving the trip circuit open after test, or to open circuited trip coils, or mechanical failure of the circuit breaker, and blown fuses in trip circuits (if used). Only a small part of the total failures occur in the protective relay itself. Thus close

* For reports see references 1, 2, 3, 8, 11, 14, 32, 37, 38, 43, and 44 at the end of the paper.

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E. L. Harder is consulting transmission engineer, Westinghouse Electric Corporation, East Pittsburgh, Pa., and W. E. Marter is relay engineer, Duquesne Light Company, Pittsburgh, Pa.

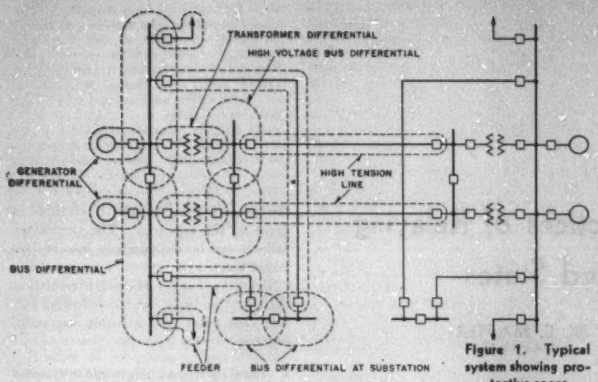


Figure 1. Typical system showing protective zones

attention to the initial design, installation, testing, and maintenance of all of the accessory equipment as well as of the protective relay proper are needed to assure successful operation.

The application of protective relays properly requires evaluation of several factors, namely:

1. The requirements of the power service and desired functioning of the system during fault conditions to produce this result.
2. The currents, voltages, temperatures, pressures, or other indicators at time of fault which provide the fundamental basis of discrimination.
3. The characteristics of available or standard relay elements.
4. The schemes in which they are used.

A wide variety of characteristics are now available operating in response to the prime quantities themselves, or to various functions of these prime quantities, such as power, phase angle, power factor, current comparison, power comparison, impedance, reactance, modified reactance, current ratio, or phase-sequence component.

In each case the response may be instantaneous, meaning no intentional delay, or the operation may depend in a predetermined manner on the electrical quantities, and time of duration.

Basic Elements

By taking some liberties with exactness and making no pretense of completeness, we can list here for reference, the more commonly used relay characteristics and their underlying principles of operation. The schemes in which the elements are used are much more numerous. The more common ones will be described under the application headings, such as generators, transformers, and busses.

Instantaneous Elements

Principles of a number of the more commonly used relay elements are shown in Figure 2. For instantaneous response to current or voltage the solenoid element, (*a*), is most common, appearing individually or as the instantaneous attachment with the induction-type overcurrent relay. The beam element, (*b*), with spring or weight bias is used where low burden is desirable, as when setting for low ground currents with low-ratio bushing current transformers. The polar element, (*j*), is of far lower burden than the nonpolar types and has come into widespread use since 1935 as the receiver relay of directional-comparison carrier equipments, and the basic element when supplied from networks or electronic devices. For example, it appears as the operating element in a pilot wire relay, in a phase-comparison carrier relay, in linear-coupler bus protection, and in supervision of pilot wires.

Because of its higher pick-up to drop-out ratio and less accurate setting the clapper-type element, (*c*), is used less frequently for the primary protective functions but is widely used as an auxiliary relay.

Induction Elements

The induction-disk element, (*d*), continues to be most widely used, its reliability and inherent time characteristic giving it great flexibility for co-ordinating relays in series or co-ordinating with fuses or direct-trip devices. A variety of characteristics are available from the definite-minimum-time, which is ideal for securing definite time steps between relays, to the very inverse which provides faster tripping with the same margins when the fault current drops considerably from one relay location to the next. The more inverse characteristic also co-ordinates better with fuses.

The induction disk serves as a directional element, (*e*), or, when used with a spring, as a watt element, (*l*), the electric torque being proportional to $EI \cos \theta$, where θ is the power-factor angle. Either of these can, of course, be current polarized instead of voltage polarized for use in ground relaying when a bank-neutral current is available.

The relative phase of voltage and current can be shifted by internal or external phase-shifting devices to produce maximum torque for power factors other than unity, as in (*g*). For example, the directional element for ground relaying usually has its maximum torque for current lagging the voltage by 60 or 70 degrees to provide maximum torques for the fault conditions. A pure watt characteristic is used with the 30 and 60 degrees connections for phase directional relays, the phase shift being provided by using the voltage of a different phase from the current. With the 90 degree connection this shift is too much and the voltage is advanced about 45 degrees by a phase shifter to provide a maximum-torque position for a current 45 degrees lagging.

The disk is provided also with an electromagnet, (*o*), producing ratio characteristics for use as a differential relay for generators or transformers. The generator-differential relay is shown in (*o*) the transformer relay having windings 1 and 2 tapped for different current-transformer ratios.

Referring to Figure 2 (*o*) the differential current produces lower-pole flux which acts in the operating direction on disk currents produced by the upper pole which is transformer fed from the same differential current. The restraint torque giving the ratio-differential characteristic, is produced by the through current in coils 2–3, which supply disk current by transformer feed to the upper pole, and by lower-pole flux produced by the same through current.

The induction disk provides a tripping-

Table I. Relay Operations

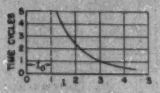
	Per Cent
Correct and desired	92.2
Correct but undesired	5.3
Wrong tripping operations	2.1
Failure to trip	0.4

Figure 2. Relay elements

Schematic | Operating Characteristics

(a)

SOLENOID, "INSTANTANEOUS", A.C. OR D.C. ADJUST BY TAPS OR COIL OR BY INITIAL PLUNGER POSITION.



OPERATES FOR $I > I_o$

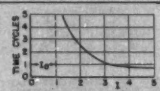
(b)

BALANCE BEAM, A.C. OR D.C., "INSTANTANEOUS", ADJUST BY COIL TAPS, AND CORE SCREW (AIRGAPS IN MAGNETIC CIRCUIT)

OPERATES FOR $I > I_o$

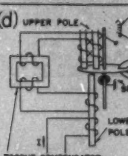
(c)

ARMATURE OR CLAPPER TYPE, - A.C. OR D.C., "INSTANTANEOUS", USUALLY FIXED SETTINGS BY SHIMS. PRIMARILY AUXILIARY VOLTAGE RELAY.

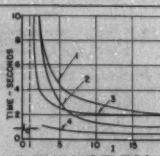


OPERATES FOR $I > I_o$

(d)



INDUCTION LISC INVERSE TIME OVER-CURRENT ELEMENT - A.C. - CURRENT IS SUPPLIED TO THE LOWER POLE, AND BY INDUCTIVE COUPLING TO THE UPPER POLE, DIRECTLY, OR THROUGH A TORQUE COMPENSATOR (SATURATING TRANSFORMER). THE UPPER POLE INDUCES CURRENTS IN THE DISK. TORQUE IS PRODUCED BY THE REACTION BETWEEN THESE CURRENTS AND FLUX FROM THE LOWER POLE. ADJUST CURRENT SETTING BY COIL TAP AND TIME SETTING BY CONTACT TRAVEL.



OPERATES FOR I GREATER THAN I_o.

TIMES SHOWN ARE MAXIMUM. RELAYS ADJUSTABLE FOR TIMES LOWS TO 0.1 OR 0.05 TIMES THOSE SHOWN.

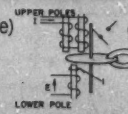
1 INVERSE, LOW ENERGY

2 VERY INVERSE, LOW ENERGY

3 STANDARD ENERGY DEFINITE MINIMUM TIME

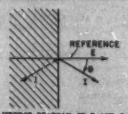
4 HIGH SPEED NO TORQUE COMPENSATOR

(e)



INDUCTION DISK DIRECTIONAL ELEMENT. CURRENT INDUCED IN THE DISK BY THE UPPER POLE REACTS ON FLUX FROM THE LOWER POLE TO PRODUCE TORQUE.

1 LAG LOOP TO BRING LOWER POLE FLUX IN PHASE WITH UPPER POLE CURRENT AT UNITY P.F. OF E AND I.



VECTOR DIAGRAM OF E AND I.

CONTACTS CLOSE FOR I IN UNBRAKED REGION, OPEN IN BRAKED REGION. TORQUE EXPRESSION

$T = KE\ I \cos \theta$

WHERE K IS A CONSTANT, θ IS THE POWER FACTOR ANGLE.

1. OPERATING CHARACTERISTICS SHOW TYPICAL ORDER OF MAGNITUDE ONLY; VARIES WITH TYPE OF RELAY AND ADJUSTMENTS.
2. SUBSCRIPT o INDICATES SETTING OR BALANCE POINT VALUE.
3. ALL CURRENT RELAYS ARE ALSO VOLTAGE RELAYS BY SUITABLE NO. OF TURNS AND EXTERNAL SERIES IMPEDANCE SINCE I = E/Z.
4. "INSTANTANEOUS" IS DEFINED AS "NO INTENTIONAL DELAY".

$$T = K_1 I_1 I_2 \cos(\theta_{12} + \phi_1) + K_1 I_1 I_8 \cos(\theta_{18} + \phi_1) + K_1 I_3 I_4 \cos(\theta_{34} + \phi_1) - K_1 I_3 I_2 \cos(\theta_{32} + \phi_1) + K_1 I_5 I_6 \cos(\theta_{56} + \phi_1) - K_1 I_5 I_4 \cos(\theta_{54} + \phi_1) + K_1 I_7 I_6 \cos(\theta_{76} + \phi_1) - K_1 I_7 I_8 \cos(\theta_{78} + \phi_1)$$ (First Order Terms)

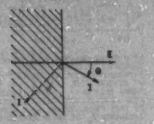
$$+ K_2 I_1 I_3 \cos(\theta_{13} + \phi_2) - K_2 I_1 I_7 \cos(\theta_{17} + \phi_2) + \text{etc.}$$ (Second Order Terms)

Schematic | Operating Characteristics

(f)

MULTIPLE POLE INDUCTION CUP OR DISK. EACH POLE PRODUCES A TORQUE PRODUCT IN CONJUNCTION WITH ITS ADJACENT POLES AND LESSER TORQUES IN CONJUNCTION WITH THOSE ONE POLE REMOVED, ETC.

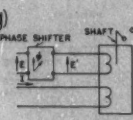
EXAMPLE: CONNECTED AS DIRECTIONAL ELEMENT (QUANTITIES IN PARENTHESIS APPLIED TO RESTRICTIVE POLES) FOR WATT CHARACTERISTICS, ϕ IS MADE ZERO.



CONTACTS CLOSE FOR I IN UNBRAKED REGION, OPEN IN BRAKED REGION. TORQUE EXPRESSION

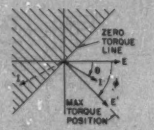
$T = KEI \cos \theta$

(g)



INDUCTION DISK OR CUP DIRECTIONAL ELEMENT AS IN e OR f, WITH PHASE SHIFT, ϕ, APPLIED TO VOLTAGE, E.

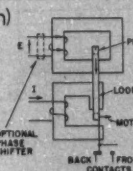
ϕ IS THE PHASE SHIFT ANGLE.



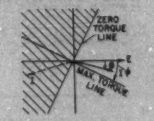
CONTACTS CLOSE FOR I IN UNBRAKED REGION, OPEN IN BRAKED REGION. TORQUE EXPRESSION

$T = KEI \cos(\theta - \phi)$

(h)

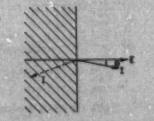


INDUCTOR-LOOP HIGH-SPEED DIRECTIONAL ELEMENT. CURRENT INDUCED IN THE LOOP BY TRANSFORMER ACTION FROM THE VOLTAGE WINDING REACTS WITH FLUX CROSSING THE GAP OF THE CURRENT ELECTROMAGNET TO PRODUCE TORQUE.



GENERAL CHARACTERISTIC

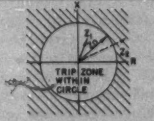
$T = KE\ I \cos(\theta - \phi)$



WATT CHARACTERISTIC ADJUSTED FOR $\phi = 0$. $T = KEI \cos \theta$. FRONT CONTACTS CLOSE FOR I IN UNBRAKED AREA. BACK CONTACTS CLOSE FOR I IN BRAKED AREA.

(i)

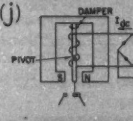
HIGH BEAM BALANCE-BEAM IMPEDANCE ELEMENT - OPERATED WHEN "CURRENT PULL" OR FRONT OF BEAM OVER BALANCES "VOLTAGE PULL" OR REAR OF BEAM. ADJUST BALANCE POINT BY CURRENT - COIL TAPS AND CORE SCREW (AIR-GAP ADJUSTMENT)



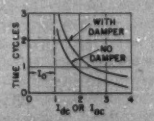
BALANCE POINT $\frac{E}{I} = Z_o$ (RADIUS OF CIRCLE) EXAMPLES: $\frac{E}{I} = Z_1 < Z_o$ TRIPS

$\frac{E}{I} = Z_2 > Z_o$ DOES NOT TRIP

(j)



POLAR ELEMENT D.C. (OR AC IS USED WITH RECTIFIER). "INSTANTANEOUS" TYPE. CURRENT IN THE OPERATING COIL, MAKES THE MOVING ARMATURE A NORTH POLE. IT IS DRAWN TOWARD THE SOUTH POLE OF THE PERMANENT MAGNET.

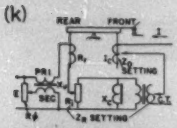


OPERATES FOR I_{dc} OR I_{ac} GREATER THAN I_o.

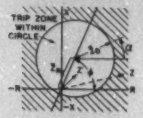
Figure 2. Relay elements (continued)

Schematic | **Operating Characteristics**

(k)



HIGH SPEED BALANCE BEAM MODIFIED IMPEDANCE ELEMENT. ADJUST IMPEDANCE RADIUS OF CIRCLE, Z_0, BY CURRENT COIL TAPS, E, AND CORE SCREW (AIR GAP). ADJUST ANGLE OF LINE ALONG WHICH CENTER IS SHIFTED BY TAPS OR RESISTOR, R_1. ADJUST IMPEDANCE Z_R BY WHICH CENTER IS SHIFTED BY TAPS ON CURRENT TRANSFORMER, C T., AND ON RESISTOR R_2.



DISPLACED CIRCLE IMPEDANCE CHARACTERISTIC

RELAY TRIPS FOR ALL FAULTS FOR WHICH IMPEDANCE, Z, MEAS BY RELAY FALLS WITHIN CIRCLE.

Z_0, Z_R, AND ϕ ARE ADJUSTABLE

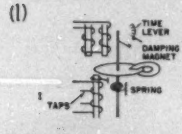
BALANCE POINT LOCUS:

$Z = Z_R + Z^{j\phi} + Z_0 + Z^{j\theta}$

FOR ALL VALUES OF θ

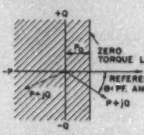
TYPICAL POWER VECTOR DIAGRAM

(l)



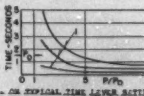
INDUCTION TYPE POWER RELAY. OPERATING TORQUE IS PRODUCT OF CURRENT INDUCED IN DISK BY UPPER POLE AND FLUX FROM LOWER POLE.

1. LAG LOOP TO BRING LOWER POLE FLUX IN PHASE WITH UPPER POLE CURRENT AT UNITY PF OF E AND I.



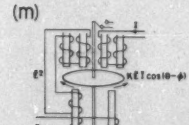
$P + jQ = I\,(P\cdot\Sigma I \cos\theta)$

CONTACTS CLOSE FOR $P + jQ$ IN UNSHADED REGION WITH TIMING AS INDICATED BELOW



1. FOR TYPICAL TIME LEVER SETTINGS

(m)



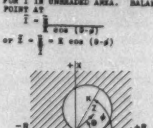
PHASE SHIFTER IF NEEDED

INDUCTION IMPEDANCE ELEMENT Z^2 PULLING AGAINST $KEI\cos(\theta-\phi)$

CURRENT TRIPPING CHARACTERISTICS WITH FIXED VOLTAGE. CONTACTS CLOSE FOR I IN UNSHADED AREA. BALANCE POINT AT

$Z = \frac{E}{I}\cos(\theta-\phi)$

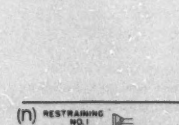
or $Z = \frac{E}{I} = K\cos(\theta-\phi)$

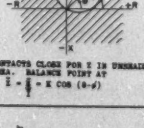


CONTACTS CLOSE FOR Z IN UNSHADED AREA. BALANCE POINT AT

$Z = \frac{E}{I} = K\cos(\theta-\phi)$

(n)



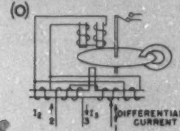


CONTACTS CLOSE FOR CURRENTS ABOVE CURVE CORRESPONDING TO RELAY SETTING.

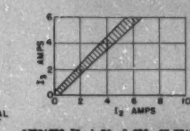
(LEFT) THREE WINDING TRANSFORMER DIFFERENTIAL RELAY. (DAMPING MAGNET NOT SHOWN) ADJUST BY TAPS ON OPERATING WINDING. (ALSO SEE FIGURE 8a)

Schematic | **Operating Characteristics**

(o)

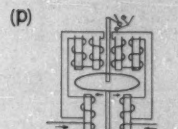


INDUCTION-TYPE RATIO-DIFFERENTIAL RELAY FOR GENERATOR AND TRANSFORMER PROTECTION.

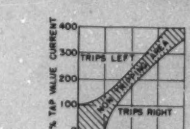


OPERATES IN .1 TO .2 SEC. ON HEAVY FAULTS. CONTACTS CLOSE FOR CURRENTS IN UNSHADED AREAS. (I_2 and I_3 APPROX. IN PHASE) SCALE SHOWN IS FOR A 10% DIFFERENTIAL GENERATOR RELAY.

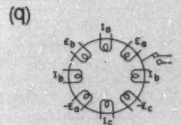
(p)



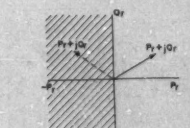
INDUCTION TYPE PHASE BALANCE RELAY (COMPACT NORMALLY SPRING CENTERED) (A SECOND DISK ON SAME SHAFT BALANCES I_a VS I_c)



(q)



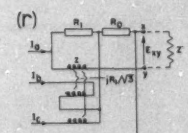
S POLE INDUCTION CUP OR WISK CONNECTED AS A POLYPHASE DIRECTIONAL ELEMENT



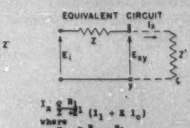
RESPONSE PROPORTIONAL TO $P_R + jQ_R = K[E_1 I_1 \cos(\theta_1 - 60 - \phi) + E_2 I_2 \cos(\theta_2 + 60 + \phi)]$

CONTACTS CLOSE FOR $P_R + jQ_R$ IN UNSHADED REGION. θ_1 AND θ_2 ARE POSITIVE AND NEGATIVE SEQUENCE PF ANGLES; ϕ IS RELAY DESIGN OR ADJUSTMENT ANGLE.

(r)



COMBINED POS.-SEQ.-CURRENT AND WEIGHTED ZERO-SEQ.-CURRENT FILTER



$I_x = \frac{E_1}{Z} = \frac{E_2}{Z'}(I_1 + K I_0)$

where

$K = \frac{3R_0 + R_1}{2R_1}$

THE INTERNAL VOLTAGE IS:

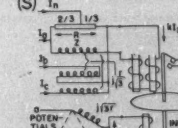
$E_x = 2R_1(I_1 + K I_0)$

THE INTERNAL IMPEDANCE IS:

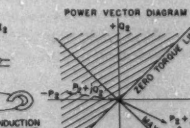
$Z = R_1 + R_0 + z$

WHERE z IS THE IMPEDANCE OF INDICATED WDG. OF 3-WDG. REACTOR

(s)

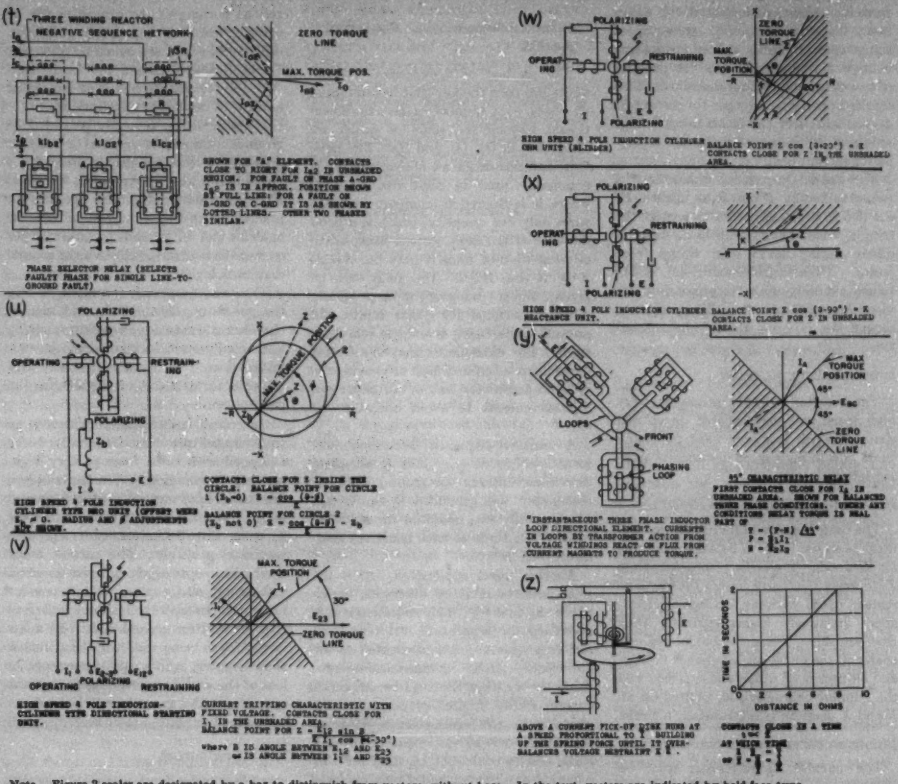


NEGATIVE SEQUENCE DIRECTIONAL ELEMENT - USING POTENTIAL, E_2, AND CURRENT, I_2, SEQUENCE SEGREGATING NETWORKS.



CONTACTS CLOSE FOR $P_2 + jQ_2$ IN UNSHADED REGION.

Figure 2. Relay elements (continued)



Note. Figure 2 scalar are designated by a bar to distinguish from vectors, without bars. In the text, vectors are indicated by boldface type.

time-proportional-to-impedance characteristic as shown in Figure 2(*s*).

Multiple Electromagnets or Disks

Two electromagnets on the disk provide for balancing mechanical torques with no phase angle effects (see (*p*)). When these are both current electromagnets, the relay is the regulating-transformer differential relay which balances the current in the shunt exciting winding against the through line current. For example in a ±10 per cent regulating transformer, it operates when the shunt current exceeds about 15 per cent of the through current. When both electromagnets are voltage energized, the voltage-differential relay results. When one is voltage (actually responsive to KE^2) and one is a product element, $EI \cos (\theta-\phi)$, a balance occurs when $I \cos (\theta-\phi) = KE$. This impedance characteristic is shown in current and also in an R-X plot in (*m*).

A second disk on the same shaft provides space for two more electromagnets. This structure is used as the phase-balance relay for motor protection, whose characteristics are shown in (*p*). It is used for the 3-winding-transformer differential relay, using one operating electromagnet and three restraint electromagnets, (*n*). With two current-input windings on each electromagnet, and using two relays per phase the multirestraint bus differential relay results. Its use will be described later.

Multiple-Pole Cylinder or Disk Elements

The multiple-pole cylinder or disk element is illustrated in (*f*). The example shows how it would be energized to act as a single-phase directional element having torque proportional to $EI \cos \theta$. This element also serves as a polyphase-directional element by the connection, (*g*).[32] The multiplepole element is flexible making possible a variety of other combinations.

4-Pole Induction-Cylinder Elements

These high-speed elements serve a variety of purposes as shown in Figure 2(*u*), (*v*), (*w*), and (*x*). The element (*u*), designated a mho element[24] operates with torque $E^2 \cos (\theta-\phi)$ restrained by torque proportional to E^2. It produces the circular-impedance-tripping locus passing through the origin or relay location, the same as shown for the induction disk in (*m*). Or with either element, the circle

can be shifted from this position by current compensation, IZ_0, in the restraining circuit, as indicated. A directional-starting unit, (*p*), is obtained using current times shifted-quadrature voltage for operating and the product of two delta voltages for restraint. This results in maximum torque for current 30 degrees ahead of the quadrature voltage or about 60 degrees lagging the unity-power-factor position.

The special impedance characteristic, (*w*), obtained by I^2 operating against $EI \cos(\theta+20$ degrees) is used to restrict the tripping area to assist other relays in differentiating heavy load swings from faults. A reactance element, (*x*), is obtained similarly with the phase-shift devices arranged so that the maximum-torque line is along the (*x*) (reactance) axis.

Inductor-Loop Element

The inductor loop, (*h*), provides a very high speed and very reliable directional element which has been used for many years now in high-speed distance measuring relays.

Balance-Beam Element

The basic balance-beam impedance element is shown in (*i*), a balance occurring for $E/I = Z_0$. For higher impedances than Z_0 (current relatively lower) the contacts remain open, whereas for lower impedances (relatively higher currents) they close quickly. Since the balance is mechanical, the phase angle between voltage and current is of minor consequence and the tripping characteristic, plotted on an R and X diagram is substantially a circle.

Modified-Impedance Characteristic

The circular characteristic may be shifted by some circuits auxiliary to the element as shown in (*k*), in order to provide better discrimination between fault currents and load and swing currents on long, heavily-loaded transmission lines. The shifting imparts a directional characteristic to the relay in addition to narrowing its tripping region to more nearly just that required for faults.

Networks and Auxiliary Circuits

It may be noted that in discussing fundamental relay elements certain auxiliary circuits, external to the mechanical relay have been introduced; in (*g*), the phase shifter; in (*j*), the Rectox; and in (*k*), a full fledged network to produce in the relay element proper, the desired currents. This is a trend of which we shall certainly see more as time goes on, as static circuits are devised to produce a simple current output proportional to the desired function of the various line currents and voltages.

Sequence-Segregating Networks, I_1+KI_0

The method of symmetrical components has been the key that has unlocked the door to a number of the aforementioned possibilities, some of which are illustrated in (*r*), (*s*), and (*t*). The positive- and zero-sequence network in (*r*), is commonly used in pilot-wire relaying, where it is desired to compare over the wires only one quantity, which is a good measure of the fault current irrespective of what kind of fault it may be, that is *A-B*, *A-Grd*, *ABC*. The relay can be given almost independent and widely different settings for phase faults and ground faults, using the single relay element. For example, it may be set for one ampere of ground fault to provide the requisite sensitivity, but for ten amperes of 3-phase current to avoid operation on loads.

A negative-sequence directional element is shown in (*s*). It is an adequate directional element for ground faults on reasonably well-grounded systems, and requires only two potential transformers rather than three as with usual residual-directional relays.

Another novel application, (*t*), is the phase-selector relay to determine which phase is faulted. This information is necessary in single-pole tripping and reclosing schemes. It is predicated on the knowledge, from symmetrical-components theory, that the negative-sequence current in the faulted phase only is in phase with the zero-sequence current. Individual overcurrent elements in the three phases could not be used for this selection as all three would pick up for a single line-to-ground fault on many solidly-grounded systems.

Alternating Current Generator Protection

Most a-c generators above 1,000 kva and many smaller machines are equipped with differential protection arranged to trip if the currents at two ends of each phase winding differ. This scheme is shown in Figure 3. Smaller machines are sometimes operated without differential protection, but if paralleled with larger machines or with a system, they are frequently arranged to trip off on a reversed flow of power into the machine for a short time interval.

For differential protection the normal-speed, induction, ratio-type relays are used in the large majority of cases, their speed (about 0.1 second relay time on severe faults) being adequate to prevent serious burning of the iron in nearly all cases. However, high-speed generator-differential relays are available providing 1-cycle protection, and are being used with 100 per cent success in an increasing number of important applications.

If the generator is solidly grounded, or grounded through a reactor or resistor, drawing at least full-load current for a ground fault at a line terminal, the usual 10 per cent differential relay operates for practically any short circuit within the machine and for grounds to within 10 per cent of the neutral, or closer if the ground current is higher.

The relay is usually arranged to trip the generator, field circuit, and neutral circuit breakers (if any) simultaneously by a manually-reset lockout relay in new installations. Frequently the relay also trips the throttle and admits CO_2 for fire prevention.

Generators feeding directly into step-up transformers are now frequently being equipped with some form of very high-resistance grounding, which draws only a few amperes of current during a ground fault. Such a scheme is shown in Figure 4, using distribution transformers with secondary resistors. The current being too small to operate the differential relay, a separate voltage relay, R, (or a current relay in series with the resistor) is used to trip the unit for ground fault. In a few cases this is being used for alarm rather than tripping, where the consequence of loss of the unit is so severe as to warrant operating with a ground until the load can be reduced or shifted elsewhere.

Field Protection

While a large number of machines still operate without any protective relays to function on loss of field, there is a trend to more general use of equipment for this purpose. On some systems where loss of field would cause serious low voltage and danger of instability, but where the system is operated in a way which can tolerate the loss of one machine, automatic means are being provided to disconnect the machines on loss or partial loss of field.

Fairly common is the use of a d-c undervoltage relay, a d'Arsonval relay in series with a resistor, connected across the slip rings for field short circuit detection; also a similar element across the field ammeter shunt for undercurrent or open field detection. These do not provide complete protection and there are a number of installations of reactive power relays used at the generator terminals in conjunction

with under voltage to trip for any field reduction which would cause serious low voltage.

On many closely knit metropolitan systems loss of field has been found to be not serious if immediately corrected. The operator attempts to restore the field, the generator in the meantime operating at somewhat reduced load as an induction generator. If he cannot restore field within a few minutes he must trip the line circuit breaker to avoid injurious rotor heating.

Field Ground Detection

Some form of field ground detection is frequently provided. It is considered most important to detect the first ground because a second could short circuit part of the field winding causing unbalance and vibration which could wreck the machine. Schemes used are illustrated in Figure 5. The a-c scheme provides complete coverage for solid grounds. The d-c scheme gives nearly complete coverage, complete if the main field rheostat is varied. In some instances vibration detectors[9] are used if the machine is known to be operating with a ground on its field. This will trip the unit instantly in event a second ground occurs. The over-all protection scheme frequently includes armature and bearing temperature indication and sometimes alarms. Less frequently field temperature indications are provided. The voltage regulators are sometimes equipped with over- and under-voltage protection and, of course, over-speed protection is provided.

Transformer Protection

As shown in Figure 6, differential protection continues to be the principal form of relay protection used in the United States for transformers of 1,000 kva and above. The normal-speed induction relays generally used are of the ratio type, having minimum-trip values of 2.5 to 3.5 amperes in secondary terms and increasing with load due to the 25 to 50 per cent ratio characteristic to reach 4 to 5 amperes at the full load point. If the current transformers are applied with 4 amperes corresponding to full load, this results in an average sensitivity of three-quarters to full load. The desirability of improving the sensitivity is generally recognized.

It is well known that the exciting current of a transformer appears as differential current, the same as does fault current, and that the inrush-exciting current, when the transformer is energized may reach a value of several times full-load current for a number of cycles. Occasional false trippings due to inrush current are encountered even with the relatively coarse sensitivities cited before. This problem is more severe when the system is very stiff relative to the transformer, that is, when the ratio of available short circuit kilovolt-amperes on the bus to the kilovolt-ampere of transformer energized is large. Magnetizing-inrush tripping suppressors are used in some cases where false tripping would otherwise be encountered.

Inrush-Tripping Suppression

One form of suppressor is shown in Figure 7. The relay is temporarily desensitized for a time interval sufficient for the inrush current to have largely died out. When the transformer is energized, voltage relays on each phase pick up and set up the high-speed trip circuit after a few seconds interval, T_2, which is long enough for the inrush current to have subsided. If a severe fault occurred during this desensitized interval, one or more of the voltage relays would remain open; the differential relays would operate, closing T_1; and tripping would occur through T_1 and the voltage-relay back contacts. An inrush, with no fault, would not drop the voltage below the settings of the voltage relays and would not trip.

A severe inrush may occur due to skipping an odd number of half cycles of voltage application to a transformer while a nearby fault is on the system then cleared. The timer, T_2, resets during this period providing the desensitizing needed to take care of the subsequent inrush.

A harmonic-restrained relay is used also to secure increased sensitivity and greater freedom from tripping on inrush. This relay takes advantage of the greater proportion of harmonic currents found in the inrush current as compared with fault current.

Differential relays of the high-speed type continue to find use where required to co-ordinate with other high-speed elements, and in other important applications. However, these constitute a small proportion of all new applications, the large majority using the normal-speed induction-type relay. In the high-speed relay the tripping suppressor is definitely required and is built in with the relay.

Gas-Pressure and Gas-Accumulator Relays

Increased attention is being given to gas-pressure and gas-accumulator relays because of their popularity in Canada and elsewhere. Some of both are being used in the United States, although at the time of the 1946 survey they were still very rare.

The gas-pressure relay, which is used for tripping, operating either on pressure or on rate of pressure increase, is claimed to offer advantages in sensitivity although this has not been proved definitely.

The gas-accumulator relay, which is used for alarm only, and only on transformers completely filled with oil, has been proved definitely very sensitive and very useful in Canada. Gas-accumulator relays are not in use on transformers having a gas cushion above the oil.

Three-Winding Transformers

For 3-winding-transformer protection, an induction-type relay having three restraining coils is generally used, and is connected as shown in Figure 8(*a*).

Regulating Transformers

Regulating transformers for voltage and phase-angle control constitute a special problem because of the change in ratio taps during operation. Figure 8(*b*) illustrates the most modern differential relay protection for such a unit. An element of the Figure 2(*p*) type (one disk, two electromagnets) is arranged to trip if the current in the shunt-exciting winding of the regulator greatly exceeds the proper proportion of the series-line current. For example with a ±10 per cent voltage regulator, a typical relay might be set to operate for any current in the shunt winding greater than 15 per cent of the line current. This provides sensitive protection for the shunt transformer. In addition normal 2-winding transformer-differential protection is applied around the entire unit, providing overlapping protection with the bus and line protection and guarding the series transformer. It is difficult to provide complete protection to the series transformer and tapped regulating windings. Optimum use is made of ground protection, for example as shown in Figure 8(*b*). However, the possibilities of this protection vary with the arrangement of windings and whether grounded or not.

Gas-accumulator and gas-pressure relays may provide the desired sensitivity for this application. However, as mentioned, the gas-accumulator as at present developed is not applicable to transformers having an inert gas cushion. The separate compartments for transformer and tap-changer would require separate elements. The sensitivity of a practical pressure element may well be greater than electrical relay protection. However, this has not been demonstrated as yet.

Co-ordination with Thermal Relays

An increasing number of transformers are supplied with thermal devices for alarm and protection under load conditions. There is a move in the United States to provide a definite co-ordination so that these will not operate faster than protective relays, during fault conditions. Selective tripping can then be secured on feeders on the secondary side of the transformers. The co-ordination figures have not been set but it appears they may be of the order of 30 or 40 seconds at 5 times normal and 5 seconds at 20 times normal. All thermal relays would operate slower than these values and all protective relays faster.

Remote Trip for Transformer Faults

Because of the high record of reliability of large power transformers, a circuit breaker between the high-voltage side of the transformer and the line frequently cannot be justified, purely for protection of the transformer, and the transformer is very little hazard to the line. However, an intermediate measure costing much less than the high-voltage circuit breaker is frequently provided to trip the remote circuit breaker (or breakers) necessary to clear in the event of a transformer fault. The transformer differential relay is sometimes used to initiate a remote trip signal over a carrier or pilot wire channel, particularly if the channel is already available for some other purpose. Another method is to close a fast spring-operated high-voltage grounding switch in response to the relay indication. This trips the ground relays at the other terminals of the line, at the expense of some added shock to the system.

Bus Protection

The advantages of bus protection in clearing faults rapidly from a system are well recognized by the industry and the provision of relay protection for major station busses has been standard practice for a number of years. The problems involved in such protection are also quite well known. One of the principal problems is the saturation of current transformers by the d-c transient component of the short-circuit current as in Figure 9. In severe cases the d-c transient component may require 100 times as much flux capacity in the transformer as is required by the a-c component to completely prevent saturation. The theory of this problem was presented before the Institute[19] in 1929.

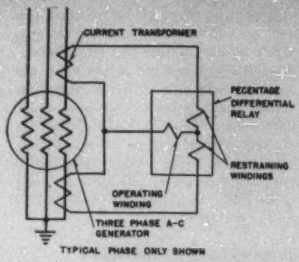


Figure 3. Generator-differential protection-connections for one phase using the percentage-differential relay for generator protection

There have been a number of successful solutions to this problem, as well as the fault-bus scheme,[12] which completely avoids it. First was the use of large current transformers which would not be saturated by the d-c component. These were used in the simple differential scheme Figure 10(*a*), with low-resistance leads to minimize the current transformer requirements. A formula was advanced specifying the requirement of iron cross section, turns and lead resistance for nonsaturation.[25] It is:

$$\frac{E_s}{R}=4.44\,IT$$

where T is the short circuit current d-c time constant in cycles. I_s is the a-c exciting current in the secondary, selected as the threshold of saturation. It would be taken as less than the relay setting by a suitable factor of safety.

E_s is the required secondary rms, a-c voltage corresponding to I_s on the a-c saturation curve of the current transformer. This determines the needed iron cross section and turns, or the iron if turns are fixed.

I is the crest value of symmetrical subtransient current, secondary amperes.

R is the secondary circuit resistance in ohms, including the transformer winding and leads up to the relay (or point at which all current transformers are paralleled.)

The a-c flux is neglected. It is usually relatively small as the ratio of maximum d-c flux to a-c flux is $2\pi T$ or 37.7 for a 6-cycle d-c time constant.

For $R=0.5$ ohm, $I=100$ amperes, $T=6$ cycles, and taking I_s as one ampere, in considering a 5-ampere relay setting; E_s becomes 1,332 volts. A current transformer which would generate this voltage at 1.0 ampere exciting current is very large. Thus, this was a bull-by-the-horns solution and the size, weight, and cost could be afforded only in the most important installations. However, it did provide the possibility of instantaneous tripping without any time delay.

A method[15] has been developed for calculating with reasonable engineering accuracy, the time-to-saturate with offset currents, and the time and current settings required to prevent misoperation with time-delay overcurrent relays and usual current transformers.

Induction Type Overcurrent Relays

On busses of moderate time constant say 0.1 second or less, and with somewhat better than average current transformers, satisfactory protection can be obtained with a straight differential scheme, Figure 10(*a*), using the fast induction element of Figure 1. A small ratio of maximum to minimum fault is favorable to this application. Relaying times of the order of 3 or 4 cycles for maximum faults and up to 8 or 12 cycles on minimum faults can be obtained in some cases. As mentioned, the performance can be predicated.[16] However, the time delays involved in less favorable cases are frequently so long as to point the need of a better solution.

Even on substation busses having a d-c time constant as short as 0.01 second false tripping has been experienced with 300/5 bushing current transformers and standard induction relays in the connection of Figure 10*a*, with 4 *A*. tap, 0.5 time lever, giving 0.15 second time at 10 times tap, and with a fault current of 13,000 amperes. While this has been overcome by changing to 1,200/5 current transformers, nevertheless present practice would be to install ratio-differential relays in these cases, providing both greater sensitivity and greater safety factor.

Multirestraint Ratio Differential Relays

Relays[20] have been developed which will not operate falsely even when used with normal sized current transformers which saturate due to the d-c component of current. These are multirestraint relays, connected as in Figure 11; however, their success is due also to exploitation of variable percentage characteristics, and the tendency of the d-c component to brake, rather than drive, the induction disk.

No small part of this development is the reduction of the operating limits to a few simple rules which insure safe application. This relay scheme provides operation generally in from three to six cycles and can be set as low as one per cent of the maximum through-fault current.

Linear Coupler Scheme

The multirestraint relays just described may, of course, be used when the setting does not need to be as low as one per cent of the maximum-through-fault current. However, on busses where a setting of six per cent or more of the maximum through-fault provides the requisite sensitivity a simpler and faster scheme (one cycle) can be used known as linear-coupler bus protection.[11,12] The linear coupler is an air-core mutual inductance used directly in the primary circuit in the same manner as a current transformer except that the secondaries are usually connected in series, as shown in Figure 10(*b*), instead of in parallel as are current transformers, Figure 10(*a*).

The secondary induced voltages are proportional to the primary currents, a ratio of five volts per 1,000 amperes being commonly used. These voltages, which add up to zero for through faults, and to a value proportional to the fault current for internal faults, are joined in a series loop to the relay as shown in Figure 10(*b*). The ±1.5 per cent tolerance within which the mutual inductances are held commercially, limits the maximum possible false differential to 3 per cent. Thus the minimum setting of 6 per cent allows a 2:1 factor of safety. Solenoid elements Figure 2(*a*) are used for settings down to about 1,500 amperes on a 6-circuit bus, and polar elements Figure 2(*j*) with saturating transformer and Rectox for lower settings.

Impedance Schemes

Busses having reactors in a majority of the feeders and possibly in the bus-tie circuits provide the possibility of protection by impedance relays.[13] Impedance or modified-impedance elements can be used, see Figure 2, (*i*) and (*k*). For a fault on the bus, the maximum impedance measured is that of the arc which is taken as about 300 to 500 volts per foot for current above 500 amperes. Considering the possible arc length during the first few cycles of fault, a maximum arc voltage can be computed and this, divided by the minimum fault current gives the greatest fault impedance encountered for internal faults, that is, for faults on the protected bus. Provided this impedance is smaller than the impedance from the relay to a fault anywhere beyond the reactors, a basis of discrimination exists and the impedance scheme can be used.

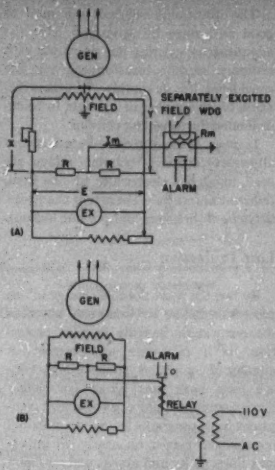


Figure 5. Field-ground detector schemes

(a) *Direct Current Scheme*

$$I_m = E\frac{(X-Y)}{(R+2R_m)(X+Y)+2XY}$$

where

E = excitation voltage at the time, in volts
I_m = relay current, in amperes
R = resistance in ohms of potentiometer resistors
R_m = resistance of relay plus fault resistance, in ohms
X, Y = resistances in ohms to point of fault from each end, including the field rheostat if used

(b) *Alternating Current Scheme*
If the bearings used do not result in a definitely and permanently grounded rotor, then a sliding contact should be connected between the shaft and ground to provide such definite grounding

Two relay arrangements have been used. When the bus tie circuits include reactors, separate impedance relays can be used on each generator or transformer feed to the bus, operation of any of which will trip the bus. This arrangement is most feasible when the generators and transformers are matched, acting as a unit, and the generators on the bus are either high and low pressure units of a single combination or are treated as a single generator. The other arrangement requires totalizing all of the main feeds to the bus and the use of a single set of impedance relays. The grouped main sources provide the possibility of a large false differential current for through faults on one of these main circuits. The voltage is also low under this condition if the source circuits are not equipped with reactors. Usually a fast induction relay, Figure 2(*d*), is used together with adequate current transformers in the main feeds so that it will not operate for through faults on those feeders. Combined impedance and fast-induction-element operation is then required to trip the bus.

Directional relays can be used in a variety of ways, either as the basic protection, tripping when fault current flows into and not out of the bus,[17] or as an adjunct for determining which of several bus sections included within a common differential protective zone is in trouble.[11]

Fault Bus

For new and certain existing segregated phase busses, the fault-bus scheme[13,14] provides a distinctly different mode of attack to the bus protection problem. All metal parts to which the bus may flash are connected together and grounded through a current transformer and relay. This construction lends itself particularly well to metal-clad switchgear. The entire cubicle or switchhouse is insulated from ground except for the connection to ground through the current

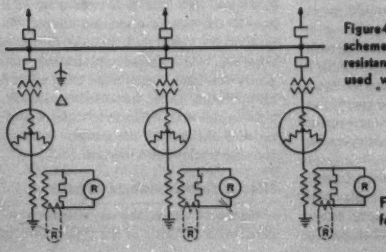


Figure 4 (left). Relay scheme for light-resistance grounding used with unit system

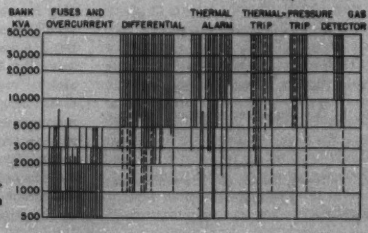


Figure 6. Transformer protection survey[13]

transformer. The simplicity of this scheme is strongly in its favor where the construction permits its use. However, it is sometimes difficult to secure overlapping protection with the adjacent system elements.

Summary of Bus Protection

While personal preference, experience, and factors peculiar to a particular installation play a large part, some of the general factors that lead to the selection of one or the other of the several schemes are described below. One-cycle operation, simplicity, and savings in cable costs, as compared with the multirestraint scheme, are favorable to linear couplers.

Quite adequate speed (3–6 cycles), the use of existing current transformers, the use of current transformers which can be used for certain other purposes also (such as back-up protection), simple application rules, and ability to set for minimum faults one per cent of maximum-through-fault, sometimes eliminating need of separate ground relay, are all favorable to the multirestraint system.

Existence of reactors and the cost or difficulty which would otherwise be involved of installing current transformers on all feeders, favors impedance schemes.

The fault-bus scheme is limited to cases where the structure can be insulated from ground, but in these cases its simplicity is favorable. Simple time delay over current frequently involves excessive delay but if used with ordinary current transformers, it may be lowest in cost.

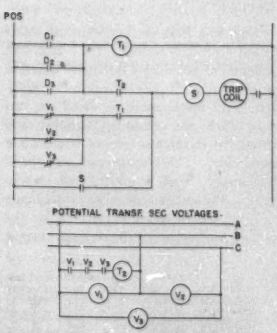


Figure 7. Inrush trip suppressor

D_1, D_2, D_3 = High speed differential relay contacts

V_1, V_2, V_3 = Voltage relays and corresponding contacts

T_1, T_2 = Timers, fraction of cycle and few seconds respectively

S = Seal in

The directional schemes are used to good advantage by some and have the advantage of securing fast operation with ordinary current transformers, but are considered less favorably by others because of the number of contacts to be coordinated for correct operation.

In most cases, spring-operated, manually-reset auxiliary tripping relays are used, unlatched electrically by the main differential relays. These trip the necessary circuit breakers and provide lockout.

Line Protection

By far, the most notable change in line protection in the last ten years has been the remarkable increase in pilot protection, both pilot-wire and carrier. A number of complete systems of high-voltage lines have been almost completely carrier equipped and a great many companies have programs leading to eventual provision of carrier on nearly all important lines, 66 kv and above, and on many lines below this voltage. Pilot-wire protection is used on short sections of high-voltage lines and extensively on the lower voltage, 13.2- to 33-kv lines, particularly in metropolitan areas. There have been impressive advances in this field including new and improved carrier[25–28] and pilot-wire[29,34] relay schemes, developments in the science and knowledge of pilot-wire protection against induced voltages[31,32] and rise in station ground potentials. There is much greater confidence and knowledge in the use of power-line carrier.

Protection of a Typical System

A composite diagram showing many of the line-protection situations which arise in power systems is given in Figure 12. The main transmission lines are shown as 220 kv, although they might be from 66 to 287 kv, or higher, bringing in power from a remote hydro-plant or interconnecting with an adjacent system. These lines are equipped with the finest and fastest protection, carrier-pilot relays although high-speed distance relays[35] might alternatively be used. In either case the modified-impedance characteristic[21] would probably be used if the lines were long or heavily loaded or both. There is some trend toward use of this characteristic generally. Balanced-line protection[35] may also be used if the lines are paired, although high-speed distance or carrier relays are required to secure fast operation with one line out of service.

The 33-kv circuits (or higher voltages), often looped or interconnected, carry bulk power out through the territory served by the particular utility, to substations in the various towns, communities, and large industries. A looped 33-kv circuit may use directional-over-current relays with instantaneous trip attachments, both phase and ground, with selectively higher time settings each way around the loop, shown at the left in Figure 12. Impedance relays are considerably more likely to be used for the phase relays, as shown at the lower right. These would preferably be the high-speed impedance type, or alternatively the normal-speed impedance relay. Directional-over-current-inverse-time, or product-type relays would probably be used for ground protection. Loops or tie circuits involving short lines of 33 or 13.8 kv lend themselves well to pilot-wire protection[29] as in the center right of Figure 12.

Induction-type overcurrent relays, usually with instantaneous-trip attachments, for operation at the higher currents, will be found on a majority of the radial feeders, and the 4- or 2.3-kv primaries. With dual 13.8-kv feeders to a substation, directional-overcurrent relays would be needed at the load end. Over-current relays with instantaneous-trip attachments are used also on higher voltage lines, the instantaneous element being set for slightly less than the maximum current for a fault at the next bus.

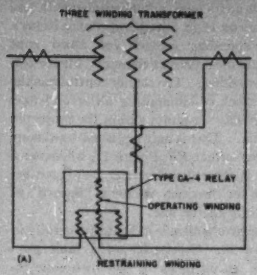
Network feeders would usually be relayed at the supply end by overcurrent relays as shown at the upper right of Figure 12 or by high-speed impedance relays. If the transformers are ungrounded, the ground relays can be sensitive and high speed, thus clearing the majority of faults very quickly. At the load ends, feeder faults would be cleared by the network relays and protectors. These relays are sensitive reverse-power relays. These several schemes of line relaying will be described later.

Over-Current

The basic schemes of co-ordination by time-over-current and time-distance are shown in Figures 13, 14, 15, and 16. The general plan of co-ordination with overcurrent relays on a radial system is shown in Figure 13. The time shown in each case is the fastest operating time for a fault at the location of the next device in sequence. At lighter generating capacity the fault currents are reduced and all operating times increased somewhat, but because of the inverse-time characteristics of the relay the margins between successive relays also increase.

Normal-Speed Impedance Relay

Figure 14 shows the co-ordination secured with elements of the type shown in



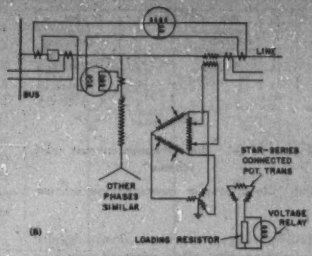


Figure 8(a). Differential protection of a 3-winding power transformer

(b) Typical large regulating transformer protection

Figure 2(*z*), on a number of line sections in series. This may equally well be a loop, the two ends shown being at the same supply point. The relay is set for a definite current pickup, much as an over current relay. When this current is exceeded, the tripping time of each relay increases in direct proportion to the distance from the relay to the fault, except that the minimum time is about 1/4-second for a fault at the relay. Each relay is adjusted to trip in approximately 3/4 second for a fault at the next bus, or 0.4* second longer than the next relay characteristic at the subsequent bus, whichever is greater. This system has been quite widely used on medium voltage lines. An advantage of the impedance system is that the characteristics do not change materially with changes of generating capacity or switching out lines.

High-Speed Impedance Relay

The high-speed impedance relay has the stepped time-distance characteristic shown in Figure 15. This can be obtained for example by separate directional, impedance, and timing elements with contacts connected as shown in Figure 17.

The impedance elements Z_1, Z_2, and Z_3 are set for successively greater distances. These elements may be pure impedance elements, Figure 2(*i*), or may have the modified characteristics, Figure 2(*k*), as will be explained later. The directional element must be high speed, such as Figure 2(*h*), and closes only for faults in the desired tripping direction from the relay. The third-zone impedance element is used to start the timer that closes first a second-zone timing contact, T_2, and later a third-zone timing contact, T_3. The timer is quick reset.

Thus for a fault in the first 90 per cent of the section, known as zone 1, the contacts D and Z_1, operate, giving instantaneous tripping, usually within one cycle, as indicated in the timing chart of Figure 15. While the other elements also operate, their action in zone 1 is purely back-up because the circuit breaker has already been tripped. Thus in zone 1 the tripping time is that of elements Z_1 and D.

For the second zone, which extends approximately to the middle of the next section (if so set), contacts D, Z_2, and T_2 in series do the tripping, provided the fault lasts for the time setting T_2. If the fault is in the next section, it will be cleared by the proper circuit breaker in advance of T_2 operation. However, back-up protection is provided by the second zone extending into the next section. This also provides operation for bus faults if they are not previously cleared by bus-protective relays.

The third zone, corresponding to tripping through the contacts D, Z_3, T_3 is set if possible to completely overlap the next section, providing complete back-up protection. It must, of course, be timed selectively with the T_3 timing of the next section.

The flexibility of this arrangement in molding its characteristic to various section lengths, circuit breaker and relay times is apparent. The highly successful operation of several thousand such relays in service indicates that for practical systems, which depart in many ways from the simple ideal case represented in Figure 15, the flexibility is sufficient to secure in general the operation outlined.

In some cases the time-proportional-to-distance characteristic lends itself better to co-ordination with other back-up protection, but the high-speed first zone element is desired. For this purpose, these two elements have been combined to make available the time-distance curve shown in Figure 16.

High-Speed Reactance Relay

A 3-zoned step characteristic similar to Figure 15, is also obtained using reactance high-speed elements, Figure 2(*x*). In this case the action is controlled by a directional-impedance starting unit, Figure 2(*v*), to avoid operation on low reactance (unity power factor) loads. Operation of the starting unit and reactance or ohm unit results in immediate tripping for first-zone faults. Second zone operation is secured by changing the ohm-unit setting at the appropriate time by means of an auxiliary relay operated by the timer. The starting unit and timer determine the third zone operating characteristic.

Ground-Relaying

The vast majority of ground relaying used in the United States is essentially overcurrent, with direction where needed. The more common elements follow:

1. Induction-overcurrent relay with instantaneous-trip attachment. The instantaneous trip is set below the maximum ground current in the line for a fault at the next bus. Nonsimultaneous closure of the circuit breaker poles during load switching may result in momentary ground current sufficient to operate the instantaneous ground relays, where the relays are set sensitively. This has been avoided by connecting a residual-voltage-relay contact in series with the trip circuit. The latter does not operate during load switching operations.

2. As in (1) but with induction-directional element, Figure 2(*e*) controlling the induction overcurrent element. The directional element may be polarized either by residual voltage or bank-neutral current if available.

3. Product-type relay, combining current and direction. (Figure 2(*l*)). Using residual current and residual voltage (or bank neutral current).

4. Instantaneous-overcurrent elements can be used if the line terminates at the far end in a transformer that will not pass residual current.

5. Less frequently high-speed-reactance-ground relaying, one relay per phase, and arranged in steps similar to phase relays in Figure 15.

While 70 or 80 per cent of the faults on lines are ground faults, except on lines with overhead ground wires, it will be noted that the relaying is predominantly of the over-current type. This is due to the greater difficulty of securing trustworthy distance operation with ground relays and the better operation of current relays in ground relaying. These relays can usually be set sensitively since the load currents if balanced do not flow through the ground relay. Also more advantageous use can be made of the inverse characteristic since ground current dimin-

* With 8-cycle circuit breakers, 0.4 second is considered adequate for circuit breaker time plus margin.

ishes more rapidly as the fault is moved from one relay location to the next.

The difficulty of impedance measurement lies in the variable fault resistance of 0 to 50 ohms or higher depending on the fault, ground wires, and tower footing resistance. Reactance relays minimize the effect of this resistance, but it is still appreciable. Mutual effects from parallel lines must also be compensated for. Thus, while reactance relays are available, their use to date has been limited.

In Canada, good success is reported with one impedance element per phase using residual current and line-to-neutral voltage. The lines are steel tower lines with ground wires.

Pilot Wire Protection

Thousands of installations of a-c pilot wire relays have been made in the last several years, a majority of which have the following characteristics in common. Some function of the line currents at the two ends of the line is compared over a single pair of pilot wires. This comparison is made at a sufficiently low energy level that leased telephone lines or private pilot wires may be used interchangeably. A network, such as Figure 2(*r*), or other means, is used to combine the line currents so as to obtain a single discriminating quantity, (E_{rp}), which can be compared over the pilot wires. The net result is a pilot-wire scheme that will clear faults within the section without any time delay and will not trip for faults beyond the current transformers at either end.

Such relays are relatively easy to apply as their settings are independent of relays on other lines. Their high-speed action simplifies the problem of setting other relays on adjacent lines.

It is necessary to have pilot wires which remain intact at times of faults on the lines protected. It is not permissible for protective gaps to break down, solidly grounding the pilot wires, to protect them from induced voltages, or station-ground potentials, caused by the flow of fault currents. If the pilot wires are subject to such voltages, these must be estimated and a proper co-ordination arranged. The voltages are kept below the breakdown of insulation or the flashover of any direct gaps to ground by one or more of the following measures.[30–33]

1. Suitable insulation of pilot wires and direct connected terminal equipment.
2. Insulating transformers.
3. Two-winding neutralizing transformers.
4. Three-winding neutralizing transformers.
5. Balance coils connecting both wires to ground or sheath through gaps, but maintaining the high impedance of the balance coils between wires.

Figure 9. False-differential current caused by the d-c saturation of current transformers during the condition of an asymmetrical short circuit

The d-c time constant for the case shown is 0.14 second. The incoming transformers are assumed not to saturate. Thus the error current of the outgoing transformer is the differential current through the relay

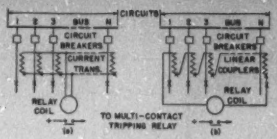


Figure 10 (a). Bus-differential-current relay scheme

(b). Linear-coupler bus-protection scheme

Carrier-Pilot Relaying

About 1,000 terminals of carrier relaying are now in use in the United States, divided chiefly between the directional-comparison and the more recent phase-comparison systems. The differences are actually of minor consequence since either system serves to provide instantaneous tripping of both circuit breakers for a fault anywhere on the protected line section. Each system requires the four elements, relays, transmitter-receiver, coupling capacitor, and line trap, as shown in Figure 18. The differences lie in the relays.

Directional Comparison System

One form of the directional comparison system utilizes the stepped impedance elements as its basic actuating elements (see section on High-Speed Impedance Relay). Corresponding second and third zone ground-over-current elements, Figure 2(*b*) are provided. The carrier control has the net effect of eliminating all second-zone time delay for faults within the protected section. This is accomplished by closing of the contact *RR*, Figure 17, whenever a fault is present and carrier is stopped because the direction of flow is "inward" at each end of the line.

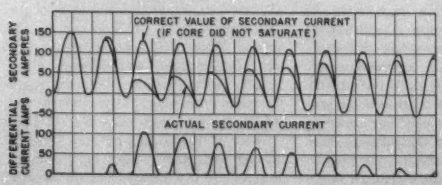
The mechanism is indicated generally in Figure 18. Occurrence of a fault anywhere within reach of the third zone relays, closes Z_3 starting carrier and setting up a circuit so that the receiver relay will close if carrier is removed by some other action. If the fault is internal, *D* and Z_3 close, stopping carrier transmission from either end of the line. The receiver relays, *RR*, immediately complete the trip circuit. If the contact circuit *RR* is opened manually the carrier can be cut out and a stepped-distance relay scheme remains. Thus the carrier is thought of as simply eliminating the time delay in the end zones, indicated by the shaded areas in Figure 15. The stepped-distance elements and an inverse-time ground current element provide the back-up protection in this system.

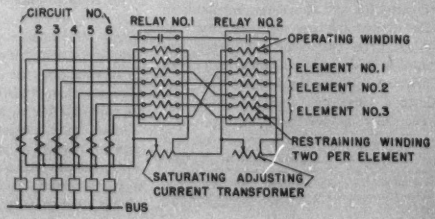
Phase Comparison System

The phase comparison system differs functionally in that the current directions or phases at the two ends of the line are compared rather than the power directions. Networks are used to derive a single-phase function of the line currents as in the pilot-wire relay. This function may be referred to simply as the "current" at each end of the line since it is a measure of the several phase currents.

If the currents at the two ends of the line are in phase and of fault magnitude, carrier is transmitted on alternate half cycles of current from either end of the line, resulting in substantially continuous carrier on the line from one end or the other. For an internal fault the current

Figure 11. Protection of a 6-circuit bus using two multirestraint relays per phase





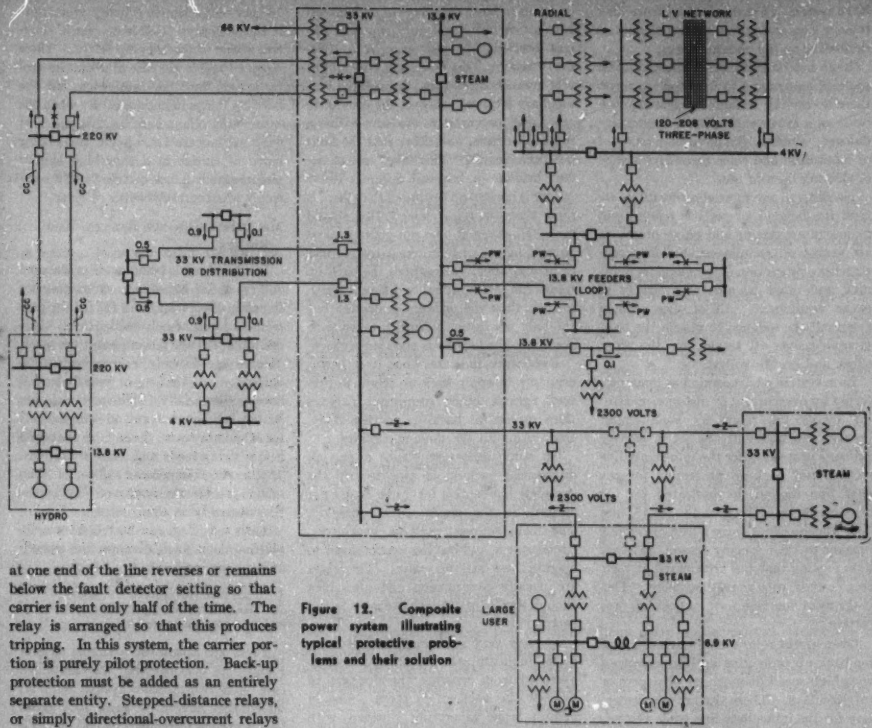


Figure 12. Composite power system illustrating typical protective problems and their solution

at one end of the line reverses or remains below the fault detector setting so that carrier is sent only half of the time. The relay is arranged so that this produces tripping. In this system, the carrier portion is purely pilot protection. Back-up protection must be added as an entirely separate entity. Stepped-distance relays, or simply directional-overcurrent relays are used for back-up protection.

A few of the factors considered in determining whether to use one or the other of these systems are the following:

Favoring the Phase Comparison System

1. Can be added for high-speed protection with any existing relay scheme for back-up.

2. No potential transformers are required. Low-tension potentials are sometimes adequate for back-up relays.

3. Inherently trip proof on out-of-step conditions. (Out-of-step blocking relays are included in the other scheme if needed.)

4. Not subject to trip by induced ground current from a parallel line.

5. Back-up relays entirely separate. Can take either high-speed or back-up out of service without affecting the other.

Favoring the Directional-Comparison System Using Stepped-Distance Relays

1. Applicable to multiterminal lines.

2. Provides better discrimination between loads (tapped from lines) and faults. When transformers are tapped along the lines, it is not desired to trip the line for faults on the low-tension feeders.

3. Can trip with fault currents less than twice load currents.

4. More flexible for system changes.

However, on many lines either system is entirely applicable and might equally well be used.

Back-up Protection

As mentioned earlier, in the event of failure of the proper circuit breakers to clear a fault, it is desirable to have some form of back-up protection to remove the fault from the system in the next best way that can be arranged within economic limits. The measures employed in present-day practice vary all the way from complete duplication of relays, circuit breakers, and tripping sources at one extreme to no back-up at all at the other extreme. The measures used in each case are somewhat a matter of opportunism as to what can be done conveniently, but more a matter of judgment and evaluation of the following factors.

1. Technical means by which back-up could be provided.

2. Importance of the section being backed up. That is, consequence to the system of failure of the primary protection, if one or the other of the various back-up possibilities is in use.

3. Cost of providing back-up protection in various ways.

4. Probability of faults, of the failure of primary protection, or of equipment failures that would cause costly or time consuming repairs if the primary protection should fail.

5. Consequences to the industrial processes or other loads being served if service is lost, momentarily, or for a long time.

To make these considerations more concrete a few examples may be cited. If a line fault hangs on due to a circuit breaker failure and is not cleared, the first hazard is that the line may be burned down resulting in a time-consuming repair job. The second is that the continued fault may cause the whole system to pull out of step and result in a complete shut down of an entire city area or

large system. Provision of back-up may remove one or both of these possibilities depending on the circumstances.

In an indoor station the failure of some piece of equipment to function properly, thereby leaving a fault uncleared, may result in a fire or explosion with extensive damage, which might have been avoided or minimized had some secondary means of clearing been in use.

Needless to say these considerations extend far beyond protective relays, into maintenance, testing and safety practices, air versus oil switchgear, building construction, protection of lines against lightning and other hazards and quality of major equipment. All of these should eventually be weighed to obtain the most favorable over-all service for the time, effort and cost involved.

In a system of transmission lines protected by overcurrent or distance-measuring relays, the relays for each circuit breaker can frequently be set to provide primary protection for the immediate line section and back-up protection for the next line section, as indicated by the characteristics of Figures 13, 14, 15, and 16. Then in event of the failure of a circuit breaker to clear for any reason, all other lines feeding that bus are cleared at their remote ends, with some time delay. This is the most common form of back-up protection.

Overcurrent relays are frequently used for back-up protection on lines equipped with high-speed distance, or pilot-wire or carrier relays. These also provide protection for the immediate line while the primary protection is taken out of service for the testing.

In many cases relays at one station, A, Figure 19, cannot reach through the bus of the next station, B, to a fault at the ends of the lines beyond. This is especially true if there is generation on the bus at station B, or if the lines beyond are very long, or both. Consideration is then given to some form of protection that will clear all other circuit breakers on bus B, if one fails to open for a fault. One scheme n use is to have the relays of each circuit breaker on bus B not only trip their circuit breaker, but also energize a time-delay relay. This relay will trip all circuit breakers on the bus, through the bus-auxiliary lock-out relay, if the protective relay does not open its contacts within a reasonable time, indicating that the fault has been cleared. This takes care of circuit breaker or trip-coil failure. Relay failure is backed up assuming that there is more than one relay element that should trip. However, it does not guard against battery failure or instrument-transformer or lead failures. Sometimes the back-up relays can be connected to different transformers than the primary protection but the only precaution normally taken with respect to the battery is good supervision.

Experience indicates that it is better economy to apply back-up relays to protect against other equipment failures than merely to back-up another relay, while using all the same accessories.

On some generator busses a partial-differential back-up is used to trip the complete bus section for feeder faults not cleared by the proper circuit breaker. The main sources, such as generators, transformers, and bus ties are totalized to operate these back-up relays. The relays are usually overcurrent and are given enough time to select with the main feeder protection.

In some very important stations complete duplicate sets of relays are used, operated from separate transformers to provide the back-up function. Also in certain large generating stations, with double circuit breakers separating major bus sections with power concentrations of the order of 200,000-kw load, the back-up partial-differential circuit not only uses separate current transformers but trips the other circuit breaker of the double-breaker combination. The partial-differential protection can often be arranged so that it backs up generator, reactor, and bus protection as well as feeder protection.

Since carrier, pilot-wire or distance protection is sometimes required to obtain 100 per cent selective primary protection, it is not always possible to obtain selective action of the back-up relays. There is some trend in the case of pilot-wire protection of short-line sections to cut the back-up relays in automatically when the supervision relays indicate that the pilot wires are inoperative. Some use is being made of carrier in a somewhat similar philosophy, to block certain back-up elements when carrier is being received.

Modified-Impedance Relays, Mho Relays

Increased use is being made of the mho and modified-impedance characteristics shown in Figure 2(u) and (k) for the protection of long heavily-loaded lines. It is desirable that the relays permit the maximum expected steady or swing loads without tripping. On long or heavily-loaded lines, and especially if high-speed reclosing is used, it becomes more and more difficult for the relays to distinguish between heavy swing loads and fault conditions. The modified-impedance relays or mho relays provide the improved discrimination necessary in these cases.

Their operation can best be shown by plotting on a single diagram the impedances corresponding to three things.

1. Faults on the protected line.
2. Heavy load swing conditions.
3. The relay characteristics.

These are shown for a simple case in Figure 20. Considering the simplified case of a two-machine system with all impedances having the same R/X ratio, let the system impedance be laid out on a resistance-reactance chart as the line $OABO'$ representing the impedance from one machine to the other. The transmission line being studied is a tie-line constituting the section AB, shown heavy of this total impedance. If R and X axes are drawn through A, as shown, the impedance with respect to these co-ordinate axes are those seen by the relays at A. Thus, the locus of impedances seen by the relay at A for a solid fault on the protected line consists of the line A-B, having

Figure 13 (left). Co-ordination of overcurrent protection on a radial power system

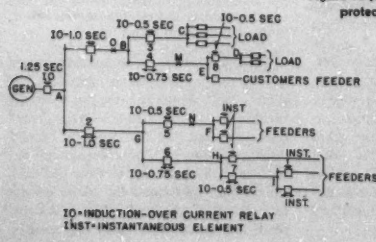
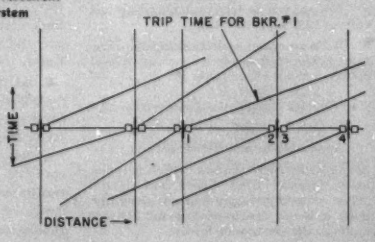


Figure 14. Co-ordination of relays having an operating time directly proportional to the distance (impedance) from relay to fault

Minimum operating time about 1/4 second



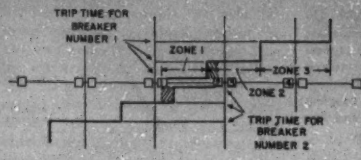
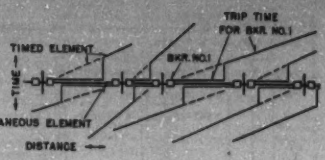


Figure 15. Ideal step-impedance-time characteristic

Figure 16. Coordination using relays having instantaneous first-zone impedance and time-proportional - to-impedance element for end-zone and back-up functions



impedances from zero up to the full line impedance.

It can be shown that if the generator voltages at the two ends of the system are equal in magnitude and are at first in phase, then are moved out-of-phase, resulting in load transfer over the line A-B, the impedance locus viewed from A is along the line L-L'. That is, the impedance seen by a relay at A, for the load condition, is the impedance vector drawn from the origin, A, to a point on the line L-L'. This line bisects O-O' perpendicularly. The no-load points, corresponding to zero angle between machines are at infinity either way along L-L', whereas the 180 degree out-of-phase condition is at the intersection, C. All intermediate loads are somewhere along the line L-L', the point L corresponding to power flow A to B and point L' to power flow from B to A.

The large circle with center at the origin, A, represents a pure impedance characteristic, as in Figure 2(*i*). The smaller circle having the same "reach" beyond B is a modified impedance characteristic, Figure 2 (*k*) or (*u*). It can equally well trip for all faults on the protected line but is less likely to trip for very heavy loads or load swings. Successively heavier loads are represented by progressing through points L, D, and E along the load locus. The modified characteristic taken with directional element, trips for faults or loads in the cross-hatched area F, while the pure impedance element trips also in the areas G and G'. Thus the modified characteristic permits heavier loads without tripping.

When carrier relaying is used so that tripping requires closure of the relay at each end of the line, the small circle, for the relay at A can be advantageously shifted further to the right, so that the combined action of this relay and its mate at the other end of the line limits carrier tripping to the restricted zone between the two arcs at H and H'. The back-up protection must be given sufficient timing to ride through swings or eliminated entirely except for a high-set, long-time element.

The modified impedance element provides for independent adjustment of the radius of the circle and the location of the center as shown in Figure 2(*k*) and hence makes possible the superior discriminating characteristics needed for long or heavily-loaded lines as outlined before. The mho-element characteristic has similar flexibility, Figure 2(*u*), but can most simply be arranged to pass through the relay point A.

Motor Protection

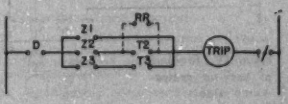
For the larger motors which are equipped with protective relays, the protection supplied varies with the importance of the service and whether automatic or not and whether attended or non-attended.

Many motors 500 horsepower and larger, and some smaller ones are protected by long-time (geared, 40-second) induction-type overcurrent relays set in the neighborhood of 150 per cent of maximum-load current, but with timing to permit the starting inrush of several-hundred-per cent current for several seconds. A typical case might be 600 per cent for ten seconds for an across-the-line-started induction motor, although these figures vary widely, depending on the application, and must be determined in each individual case. These relays will operate for a stalled motor for internal motor faults or for a heavy overload.

The overcurrent relays are usually provided with instantaneous-trip attachments set above the motor-starting current to trip quickly for severe faults in starter, leads, or motor.

Figure 17. Contact circuit for producing the stepped time-distance characteristic shown in Figure 15

Z_1 = first-zone impedance-element contact
Z_2 = Second-zone impedance-element contact
Z_3 = Third-zone impedance-element contact
T_2, T_3 = Timer contacts having separately adjustable time settings for second and third zones
D = Directional-element contact



Thermal relays are frequently provided in addition to the overcurrent relays, to provide more sensitive overload protection. Having a time constant of several minutes as contrasted with seconds for the overcurrent relay, they follow the motor temperature more closely. They will not protect the motor under stalled conditions, however, where the motor ventilation is missing, the overcurrent relay provides this function.

In automatic applications, and where starting in reverse would be serious, a phase rotation relay is used which closes contacts to permit a starting only if the phase sequence is correct and voltage is present on all three phases. This does not guard against phase unbalances which might occur during operation, however. For this purpose, a phase-balance current relay, Figure 2(*p*), is provided in many automatic schemes to trip if the 3-phase currents become excessively unbalanced. Time-delay under-voltage protection is frequently applied to guard against overcurrent or process damage, resulting from sustained operation at low voltage. However, where continuous operation is more important than motor protection this feature is eliminated or used for alarm only.

Thermal-alarm devices applied directly on the motors are becoming increasingly popular in attended stations where the operator can determine from the ammeters or other indications, the source of trouble.

Power House Auxiliaries

The power house is the heart of any electric system and its functioning rests on the motors which drive its fans, pumps, gates, and other auxiliaries. Hence reliability is paramount and in addition to provision of a most suitable power supply and spare units for certain auxiliaries, much attention has been given to the relay protection. A recent survey of United States practice[42] resulted in the following recommendations, attendance being assumed.

The 2,300-volt motors should use long-time-delay phase-overcurrent relays for overload and internal motor faults, set at approximately 150 per cent of rated current.

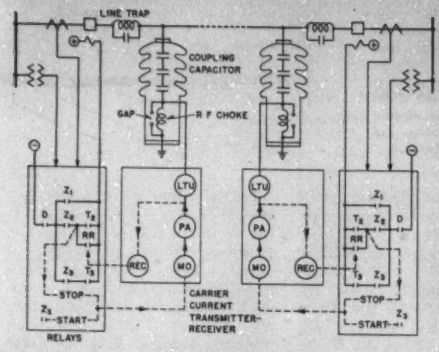


Figure 18 (left). Directional-comparison carrier-relay system including relays, carrier transmitter-receivers, coupling capacitors, and line traps

Dotted lines indicate symbolically the carrier controls

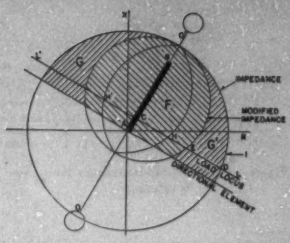


Figure 20. Modified-impedance characteristics provides improved selectivity between heavy-load swings and line faults

They also should be equipped with instantaneous overcurrent relays for short-circuit protection set above maximum-inrush current. If the auxiliaries are transferred the instantaneous relay must be set above a higher inrush current.

On essential motors* the time-delay overload relays may be used for alarm purposes only, and the instantaneous relays used to trip. In this case the time-delay relays can be set more sensitively than 150 per cent of rated current.

Low voltage motors (208, 440, 550 volts) should use a thermal device for overload protection, and an instantaneous-trip device for short-circuit protection.

In addition, the report notes the desirability of eliminating undervoltage protection except for alarm purposes, so that the loss of auxiliaries due to system disturbances will be minimized.

General Subjects

Brief mention will now be made of certain subjects and special relay problems in order to partly fill some large gaps still remaining in this picture of protective relaying in the United States.

Industrial Interconnections

When a line is tapped to an industrial plant having generation, it is common practice to segregate essential loads for operation from the plant generator and dump others in event of a line outage. If the same line is tapped for other plants, the problem arises of separating the plant under consideration from the line under conditions hazardous to its operation. One scheme in successful use on many industrial interconnections consists of separation based on any of three indications provided power flow has reversed and is toward the power company. The three indications are: under frequency, under-voltage, or generator overload. Any of these occurrences, provided power flow is away from the plant, is taken as sufficient cause for separating and at the same time dumping nonessential loads so that the remaining plant load may be brought within the capacity of the plant generation.

The relays normally employed are:

Induction-type overcurrent for generator overload.

Induction-type under-frequency relay.

Induction-type undervoltage relay.

High-speed-type 3-phase directional relay.

The generator overload relay is directional controlled so that it will not start timing unless direction in the interconnection has reversed.

Directional relays are also used, without the voltage, frequency or current fault detectors, for this purpose.

Three Terminal Lines

Lines having three or more terminals[26] are generally more difficult to protect than 2-terminal lines. Alternating current pilot wire protection is applicable in many cases although the limiting values of pilot-wire capacitance and resistance are less per terminal than for 2-terminal lines. Carrier schemes, particularly of the blocking type are applicable to multi-terminal lines. However, sequential operation of circuit breakers occurs if fault current of appreciable magnitude flows out at one terminal for an internal fault near another terminal. The first-zone impedance element nearest the fault, acting independent of carrier, opens the first circuit breaker, after which carrier is stopped by the directional elements permitting clearing of the other circuit breakers.

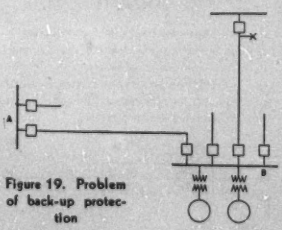


Figure 19. Problem of back-up protection

Out-of-Step Protection

Practically all utilities,[26] except those consisting of steam stations connected rigidly together electrically, have experienced system instability. Most utilities have experienced some undesired operation of fault-protective relays as a result of system instability. Quite a number of utilities attempt either to block line relays from tripping because of out-of-step conditions, or to set the relays so that tripping will occur at a preselected point. Out-of-step blocking on conjunction with carrier relaying is the method most commonly employed.

Synchronous frequency changers interconnecting two systems may suffer mechanical damage to shafts and couplings if permitted to operate with the systems out-of-step. The resulting power pulsations may be close to the natural frequency of the two-mass system composed of the two rotors with connecting shaft. Out-of-step relays are available which detect a slip cycle by the power reversal at high current and can be set to trip after two or three slip cycles, or before serious torque oscillations build up.

Quick clearing of faults by modern 8-, 5-, and 3-cycle circuit breakers and high-speed relays is well accepted as a measure of prime importance in improving system stability and reducing damage and permanent outages. Case after case could be cited where these improvements have been realized as circuit breakers and relays have been modernized up to present-day standards. High speed reclosing

* Essential motors are in this case defined as those motors whose failure results in the shut-down of generating capacity.

has been made possible by simultaneous operation of circuit breakers at the two ends of a transmission line by carrier-current or pilot-wire relaying. This measure is generally accepted as economically of greatest benefit in improving stability and service reliability. Three-pole[41] reclosing has been most widely used. However, there are a number of applications of single pole reclosing[42] which further enhance the stability by leaving the sound phases in service while the faulted ones are opened and reclosed.

Testing and Maintenance

Routine tests are made by many companies at quite frequent intervals such as one to three months, depending on the importance of the service. However, the major calibration tests are generally scheduled for periods more of the order of six months to two years. One year is a quite common period. There is a decided feeling that too frequent testing may cause more harm from mistakes and inadvertent damage than the good that is accomplished. The tests vary from the over-all or primary test in which current is passed through the primary of the current transformer, and the circuit breaker tripped by the resulting relay action, to much less complete checks. A quite usual procedure would be to remove the relays from service and test and calibrate on a load box, and to check the instrument transformers for continuity and grounds. The instrument transformer-relay circuit is grounded at only one point[44] so that the intentional ground can be lifted for this test. If feasible the circuit breaker may be tripped by closing the relay contact.

Conclusion

An attempt has been made to present a good many of the highlights of protective relaying as it is now practiced in the United States. Space has prevented even mentioning a great many schemes in successful use. It has been necessary to restrict the discussion to a few which the authors believe to be the more common. Quite copious references have been given to enable the reader to pursue further the subjects of particular interest. The three bibliography sections of the AIEE relay committee[1–3] are highly recommended as a guide to other relaying subjects of interest which are not touched on in this paper.

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Discussion

C. E. Asbury (Commonwealth and Southern Corporation, Jackson, Mich.): This is a most excellent paper covering the principles and practices of the more commonly used relaying schemes in the United States. The concise descriptive material in the body of the paper, as well as the list of references and supplementary diagrams at the end of the paper, giving graphical representations of relay characteristics and fundamental elements of design, should prove to be a very useful reference.

As pointed out by the authors in their conclusion, no doubt due to space requirements, it has been necessary for them to limit mentioning a great many schemes which are in successful use. I wish to emphasize one type of bus protection to which the authors make a reference on page 8 of their paper. This scheme, referred to further in Steeb's paper which is listed as reference number 17, consists of instantaneous ground relays on each oil circuit breaker off the bus to provide an over-all bus ground protective scheme. Our general plan is to use instantaneous initiating ground relays on each circuit breaker having a source of ground current, and connect all of their contacts in parallel. The combination of these initiating relay contacts in parallel is connected in series with the back contacts of an instantaneous relay on each of the circuits connected to the bus. These back contacts provide blocking of the bus protection scheme for external faults. Directional blocking relays are required on feeders having ground sources. A time-delay relay is used to co-ordinate the initiating relays with the blocking relays to make certain that the blocking relays have time to open the circuit before the initiating relays close. Since this arrangement operates on the basis of individual instantaneous ground relays on each circuit breaker and does not have the current circuits interconnected, the bus protective scheme is independent of current transformer characteristics, which is very desirable. We have a number of these bus protective schemes in service at the present time and are well pleased with their performance.

A vast majority of protective relays in use in the United States are overcurrent, or directional overcurrent relays. In the major transmission systems, these overcurrent relays are being replaced rapidly by various forms of distance relays having greater load and swing ability. However, for economic reasons, there still remain a number of lower voltage systems where overcurrent and directional overcurrent relays are applied. During the past four or five years, most systems have been operated heavily loaded, and it has been necessary to increase settings on these overcurrent and directional overcurrent relays.

In many applications the limiting condition is load ability. That is, an upper limit is reached above which the relay cannot be set, and still provide proper system protection for minimum fault conditions. This setting may not be high enough for emergency loading conditions. A new relay has recently been developed to alleviate this situation. A modified impedance element is used for the directional element, and an overcurrent element is used to provide inverse time overcurrent characteristics to give selective tripping. The directional element determines the load ability of the relay and has essentially the same directional characteristics as those shown in Figure 2*M* of the paper. We are at present installing a number of these voltage restrained directional overcurrent relays for increased load ability and we believe they will find a wide application. Where overcurrent relays require greater load ability, instantaneous overcurrent and instantaneous undervoltage fault detectors are used. Contacts of these two fault detectors are connected in parallel and the combination in series with the overcurrent relay contacts.

As pointed out by the authors, many applications of instantaneous phase and ground relays have been made to supplement inverse time overcurrent phase and ground protection to provide high-speed protection for a portion of the line section. The instantaneous element is normally set below the maximum fault current at the next bus, and where these are not directional, it is necessary that they be set high enough not to operate for faults on the bus or lines behind the breaker. This latter condition often requires such a high setting on the instantaneous attachment, that little line protection is obtained. In some cases this inflow of fault current may be of such a magnitude as to prevent the application of instantaneous attachments.

Directional controlled instantaneous relays have recently been made available. These relays are available for both phase and ground protection, and extend greatly the application of instantaneous overcurrent protection. These directional controlled, instantaneous relays may be applied to lines where the infeed of short circuit current (the nontripping direction) is many times greater than the fault current on the line section (current in the tripping direction). We are using a number of these directional controlled, instantaneous relays for ground protection, and it appears that they will find wide application.

Eric T. B. Gross (Illinois Institute of Technology, Chicago, Ill.): The authors have done an excellent job in summarizing present-day relaying practices in this country. Their paper is a valuable addition to the literature. It will be helpful to both the practicing engineer and the student for whom relaying is a new subject.

In order to make the chapter dealing with generator protection more complete, I would like to suggest that a few words be added about the various methods of de-energizing the field of generators on operation of any of the protective relays, since such equipment is often considered a component of the relay scheme. It may also be mentioned that solid neutral grounding of generators is not considered good practice at the present time.

As for transformer protection, it seems to me that the recent report of the relay committee (reference 15 of the paper) indicated difficulties with differential protection due to magnetizing inrush currents, which do not seem to have been eliminated completely by the methods and special relays mentioned in this paper.

In future installations, consideration should be given to a combination of pressure-and-gas-actuated relays, since such relays are much more sensitive[1] than any kind of differential protection, free from the influence of inrush currents, and because they present an economical solution for smaller transformers. It is doubtful whether regulating transformers require two gas-actuated relays, since the development of gas in the separate tap-changing compartment is as natural, and without harm to proper designs, as the development of gas in an oil circuit breaker. Though protection is required for the transformer as such, it is hardly necessary for the rugged tap-changing mechanism, if the tap changer is placed in a separate compartment.

The difficulty of securing trustworthy distance protection on ground faults is open to question, since good results with relay schemes, originated by Dubuse and Douce, have been obtained in this country and abroad, in which line currents, residual current and line-to-ground voltages are fed to the impedance relays.[2,3] Schemes which apply selector relays[4] in addition seem to have distinct advantages over a combination of distance relays (for line-to-line faults) and inverse time relays (for ground faults), and would deserve wider application than they had in the past.

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E. L. Harder and W. E. Marter: The discussors have added useful information and comments to the paper which is necessarily brief on many subjects. Gross mentions that solid grounding of generators is not considered good practice at the present time.

The paper makes no recommendation in this regard. The recent report on present day grounding practices[1] mentions that 25.7 per cent of generating capacity is solidly grounded. It is the protection of these machines which is being discussed under that heading.

In general we concur with Gross' comments about pressure and gas actuated relays although we caution against misinterpretation of the statement that pressure and gas actuated relays are more sensitive.

It is the gas actuated relays used for alarm devices and not for tripping which are more sensitive. The pressure devices used for tripping have not been definitely proved more sensitive. In general,

alarm devices of various types have not been included within the scope of the protective relaying paper.

We concur that progress is being made in distance ground relaying.

Asbury has mentioned some recent relaying applications not mentioned in the paper which are of considerable importance. The paper covered only the more widely used schemes of protection.

REFERENCE

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A Method of Selecting Inherent Overheat Protectors Based Upon Motor Design Information

V. G. VAUGHAN
ASSOCIATE AIEE

WHEN the first motors were designed, it was entirely by cut and try methods. There was little or no knowledge about current carrying capacity of wires, rates of heat loss, magnetic characteristics of iron, and so forth. Today with the detailed information now available, it is possible to calculate for a desired performance with a reasonable degree of assurance that the expected performance will be attained.

Much of this information was obtained by building motors and running many tests. The knowledge gained by study of the data from these tests has produced the necessary detailed information for the modern methods used in designing and forecasting the motor performance.

Inherent overheat protectors are a relatively new product. They are just emerging from the trial and error stage. When one has the protectors there is still the design problem of applying the protector to the motor to produce the best engineered combination. As the protectors are emerging from the trial and error stage it seems desirable to start thinking about ways and means of reducing the problems of application of the protector to the motor to some sort of a scientific standard.

The object of this paper is to stimulate thinking in this direction and to describe a method developed for the selection of protectors by using motor design information for single phase fractional horsepower motors.

As in all preliminary investigations the results were obtained by testing motors, protectors, and the combination. Then by working backwards, empirical curves have been established for arriving at the proper protector to select for use in other motors of the same design as those used to produce the curves.

History of Inherent Overheat Protection

Inherent overheat protection can be divided into two basic types.

1. Where the thermal element is responsive to temperature only. This type of device has been used for years in large electric machines in the form of thermocouples and resistance coils.

2. Where the thermal element is responsive to both the flow of motor current through the protector and the ambient temperature surrounding the protector.

The writer's knowledge of such a device began with experimental work in 1924. At that time motor engineers were reluctant to consider making any provisions in the motor for mounting the protectors. Up to that time fractional horsepower motors depended on fuses, thermal cutouts or relays mounted remotely from the motor for thermal protection.

The first commercial application was made about 1929 to one of the early hermetic refrigerator machines. It is interesting to note that this original design and method of application is still in use by this manufacturer and his motor life in the field has been one of the best.

WHAT THE PROTECTOR SHOULD DO

From its name it is obvious that it should protect. If it is to be of benefit to the motor user, it must protect without limiting the motor's ability to do the maximum amount of work safely. This indicates that if we are to protect motors, we must know something about those features of a motor that will result in excessive temperatures.

Motor Temperatures

It is needless to go into the history of motor ratings based on temperature rise. It was introduced in the days when motor production was small and this method of rating was necessary to create a standard for the industry. It has been in use so long that it has become a tradition.

In order to keep this paper as simple as possible, let us confine our discussion to class "*A*" insulated motors. The AIEE standards say that class "*A*" insulation may be continuously used at temperatures up to 105 degrees centigrade. We know that the life of insulation is a function of its continuous operating temperature; the higher the continuous temperature, the shorter the life. This is illustrated by curves in Figures 1 and 2.

Figure 1 shows an average life of 2,000 days at 105 degrees centigrade, whereas Figure 2 shows a minimum life expectancy of 2,000 days at 105 degrees centigrade. The two authorities show the same basic time-temperature characteristic and only vary as to degree.

From Figure 1 we note a 2,000 day average life expectancy at 105 degrees centigrade and yet at 200 degrees centigrade, we see a minimum life expectancy of five days.

Due to the wide difference in values at the extremities of the curves in Figures 1 and 2, there is some feeling that a composite curve should be developed. The author suggests that this could well be an activity of the AIEE committee on insulation. Figure 3 is an attempt to further illustrate the fact that short periods at

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V. G. VAUGHAN is vice-president, Spencer Thermostat Company, Attleboro, Mass.

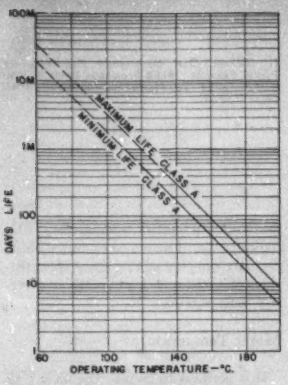


Figure 1. Effect of temperature on life of insulation (Scott and Thompson)

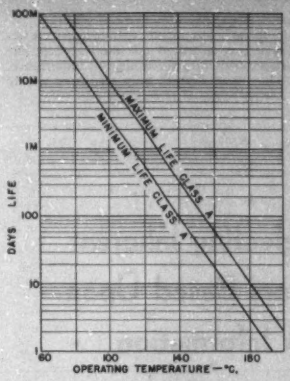


Figure 2. Effect of temperature on life of insulation (Miner)

higher than normal temperatures are no more destructive than longer periods at lower temperatures. The white area indicates the author's conception of how insulation life is reduced with higher temperatures.

In Figure 3 we have a line for 40 degrees centigrade ambient, the base line for rating motors. Then, above this is the 80 degrees centigrade line for full-load rating of open-type motors based on 40 degrees centigrade rise in 40 degrees centigrade ambient. When the motor is operating at temperatures of 80 degrees centigrade or lower, it is assumed that the motor is operating at full load or less than rated full load.

Above the 80 degrees centigrade we have an area with vertical lines that denotes a range of temperatures where it is assumed that the motor is operating with some degree of overload.

The vertical lines stop at 105 degrees centigrade which is the AIEE maximum allowable safe continuous operating temperature. It is recognized generally that the class "*A*" insulated motor can safely operate continuously at any temperature below 105 degrees centigrade.

Above the 105 degrees centigrade we have heavily shaded areas that taper with ascending temperatures. These shaded areas indicate that as class "*A*" insulation is subjected to temperatures in excess of 105 degrees centigrade, the operating time cannot be continuous and must be reduced, if we are to maintain a life equivalent to continuous operation at 105 degrees centigrade. This is accomplished by intermittent operation of the protector.

Temperature Limitations of Protected Motors with Class *A* Insulation

The preceding facts are the basis for the Underwriters' Laboratories adopting for inherent protected class "*A*" insulated motors 125 degrees centigrade as the maximum tripping temperature on running loads and 150 degrees centigrade as the peak temperature on stalled motor.

Motor Hot Spot Temperatures

Our temperature discussion up to this point has been general. It is well known that when a current flows in the motor winding, the temperature is not uniform.

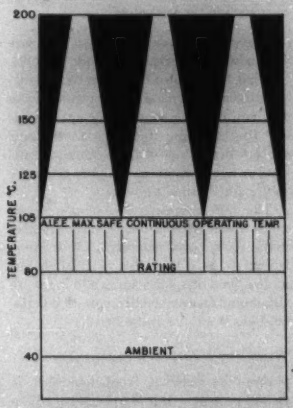


Figure 3. Effective winding temperatures of protected motors

There is one spot that will be hotter than the rest of the motor and it is recognized generally that it is this hot-spot temperature that must be considered as the limiting temperature.

Temperature Measurement Methods

The traditional method of measuring motor temperatures has been the mercury thermometer. This method is neither sufficiently sensitive nor accurate for obtaining hot-spot temperature information for use in inherent overheat protector application work.

Temperature Rise

It has been traditional to assume that a given load current will produce a uniform temperature rise in the motor regardless of the ambient in which the motor is located. This assumption is not sufficiently accurate for inherent overheat protector application work as is seen in curves and table in Figure 5. The details of Figure 5 will be discussed later in the paper.

Temperature Information

It has been customary for motor engineers to make temperature tests at full load and at 125 per cent of full load.

For protector application work it is desirable to know more about the temperatures produced by load currents all the way up to the current to raise the winding to 125 degrees centigrade.

All of the necessary temperature information can be obtained from a curve

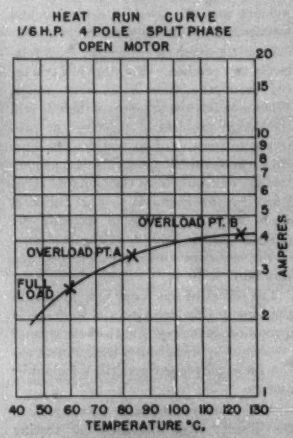


Figure 4. Heat run curve, 1/6 horsepower 4-pole split phase open motor

(Figure 4) plotted from data obtained by running the motor until stable temperatures are reached; first at full load current, second at a value "A" of overload current and third at a higher value "B" of overload current as shown on the curve.

In order to obtain a good degree of accuracy these tests should be run in a room where the ambient is maintained at a constant value. The necessity for this is illustrated by curves in Figure 5. These curves are plotted from results obtained by first running at various values of load in 26 degrees centigrade ambient and then in 40 degrees centigrade ambient.

These curves clearly indicate the degree of error in assuming that a given value of current will produce the same temperature rise in a motor regardless of room temperature. The higher the motor temperature, the greater this error. This suggests that some day a new method of rating motors must be devised. In addition to running temperatures, information must be available on locked rotor temperatures. Under locked conditions it is well known that the motor will overheat in a short time so the necessary locked rotor information is one of time versus temperature. This is illustrated in Figure 6.

As the time required to heat the winding is very short, a standard test method has been adopted in order to get uniform results. Temperatures are measured by thermocouple, locked current is produced by a constant voltage source, and the tests are made in a constant temperature room. The recording of data is not started until the windings stabilize at test room temperature. After recording initial temperatures, the potentiometer is set ahead for a 40 degrees centigrade reading. When the potentiometer indicates that the winding has reached 40 degrees centigrade, the time and current data are recorded and the potentiometer is set ahead for a 60 degrees centigrade reading, and so forth.

Table I. Locked Rotor Test on 1/6 Horsepower Split Phase Motor Run in Constant Ambient 26 Degrees Centigrade With Constant 115-Volt Supply

Open Motor			Enclosed Motor		
Ampere	Time, Second	Start Winding, Temperature Degrees Centigrade	Ampere	Time, Second	Start Winding Temperature Degrees Centigrade
0	0	25	0	0	27
17.8	3	40	18.0	2	40
17.4	5	60	17.6	5	60
16.5	8	80	17.1	8	80
16.1	12	100	16.6	12	100
15.6	16	120	16.0	16	120

Mechanical Factors Affecting Motor Temperature

Motors are built with many mechanical variations such as open or enclosed, with or without fan, castiron or aluminum end shields, and various methods of mounting. Few, if any, of these variations have any effect on the initial locked rotor heating characteristics, as shown in Table I.

The tests that produced these data were run as described in the preceding paragraph. The locked rotor curve for the open motor test is plotted in Figure 6. The test on the enclosed motor was made after the openings had been securely taped closed so as to prevent any ventilation. It is to be noted that the time-temperature data for both the open and closed rotor tests (Table 5) are practically the same.

Nearly all mechanical variations do affect the running temperatures as illustrated by heat run curves in Figures 7, 8, and 9.

Motor Line

In fractional horsepower motors it is common practice for each manufacturer to have a standard external diameter punching for a group of motors; as in this example from 1/20-horsepower, 8-pole motor up to and including 1/3-horsepower, 2-pole motor. The internal diameter of stator punching and rotor diameter for 8-, 6- and 4-pole motors are the same. The 2-pole motor usually has a smaller rotor and a smaller internal diameter in the stator punching.

For the smallest rated motor of a given speed, the wire is of relatively small diameter and the shortest stacking of laminations is used. As the horsepower for this speed is increased the wire size is increased and a greater amount of iron is required. An example of this is shown in Table II.

Any of these motors can be produced with any of the possible mechanical variations. Obviously, as mechanical variations are introduced which will affect motor temperature, it may be necessary to change the size of wire used in the winding.

		AMBIENT 26°C. WINDING		AMBIENT 40°C. WINDING	
	AMPS.	TEMP.	RISE	TEMP.	RISE
NO LOAD	0	26	0	40	0
FULL LOAD	1.93	46	20	56.5	16.5
	2.6	58.5	32.5	68	28
	3	69	43	76.5	36.5
	3.5	85.5	59.5	92	52
	4	108.5	82.5	112.5	72.5
	4.25	124.5	98.5	127.5	87.5

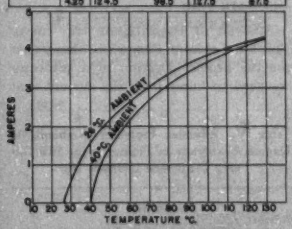


Figure 5. 1/6 horsepower 4-pole motor with cast iron end bells

Comparison of winding temperature in 26 and 40 degrees centigrade ambient

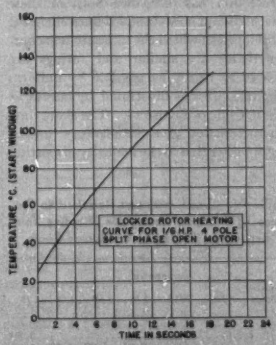


Figure 6. Locked rotor heating curve for 1/6 horsepower 4-pole split phase open motor

Table II. Stack Lengths of Iron in Inches for Various Speeds and Horsepower

This Shows a Standard Line Which Normally Is Expanded by Several Hundreds of Winding Modifications

Number Poles	Horsepower 1/20	1/12	1/8	1/6	1/4	1/3
2						2-3/8
4			1-1/2	1-1/2	2	2-3/8
6		1-1/2	2	2-3/8		
8	2	2-3/8	2-3/4			

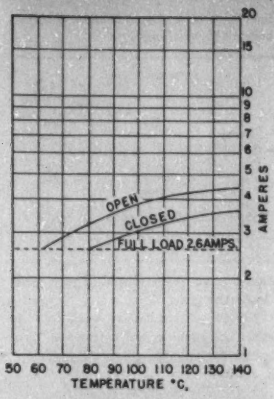


Figure 7. Comparison of open and closed heat runs on 1/6 horsepower 4-pole split phase motor

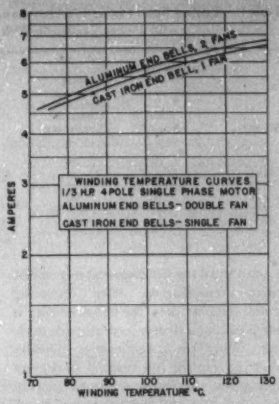


Figure 8. Winding temperature curves 1/3 horsepower 4-pole single phase motor

Aluminum end bells—double fan
Cast iron end bells—single fan

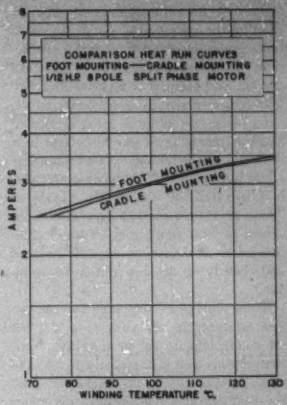


Figure 9. Comparison heat run curves foot mounting—cradle mounting

1/12 horsepower 8-pole split phase motor

Correspondingly, if the mechanical arrangement of all of these motors is kept the same, we could expect that the winding temperature would be related to wire size. In order to investigate this a series of heat-run curves similar to Figure 4 were run on such a line of motors. Table III shows the wire sizes of these motors.

The amperes which raise the winding temperature to 115 degrees centigrade in a room ambient of 26 degrees centigrade is recorded in Table IV.

If the circular mills are plotted against amperes required to reach 115 degrees centigrade, the curves in Figure 10 will be the result showing the expected relationship between wire size and running temperature.

In other words, if we had to design a new motor in this line with special characteristics, and if we knew the wire size, we could forecast the current which would heat the winding to 115 degrees centigrade in a 26 degrees centigrade room.

Table III. Wire Size in Circular Mills for a Standard Line

Circular Mils Are Actual Measurements and in Most Cases Are Less Than Standard Wire Diameters Due to Stretching During Winding

Number Poles	Horsepower 1/20	1/12	1/8	1/6	1/4	1/3
2						2,965
4			1,505	1,714	2,450	2,634
6		912	1,183	1,428		
8	676	1,024	1,428			

Protector Information

When inherent overheat protection was first introduced, it was common practice to develop a particular protector for a particular motor application.

As the art developed the next step was to produce a line of protectors embodying all possible combinations of available parts. The first method was so restrictive that it made application prohibitive in cost.

The second method was satisfactory as long as the number of combinations was not too great. Figure 17 shows the various physical sizes of disk-type protectors available.

At the present time inherent overheat protection has outgrown both of these methods and now standard lines are being developed in which the ultimate trip characteristics are similar and ratings are spaced as nearly as possible by uniform percentage steps illustrated by curves at the left side of Figure 11. The short time characteristics give satisfactory locked rotor protection to the majority of existing commercial motors. This is illustrated by the series of curves at the right side of Figure 11.

There will always be some motors in which the short time characteristics of the standard line of protectors will be either too long or too short. For these motors it will be necessary to use a special protector. Figure 14 illustrates how today, with practically the same ultimate trip characteristics, the short time can be controlled over a rather wide range. The solid line curves in the center represent the standard line protector. The broken line curves at the top represent the maximum short time possible with a special protector for the illustrated ultimate trip, and the broken line curves at the bottom represent the minimum short time possible to obtain with a special protector of approximately the same ultimate trip rating.

Ultimate Trip

Analysis of the ultimate trip curves (left-hand side of Figure 11) shows that for the proper selection of a protector we need to know both the current that

Table IV. Load Amperes Required to Produce 115 Degree Centigrade Winding Temperature in 26 Degree Centigrade Room

Number Poles	Horsepower 1/20	1/12	1/8	1/6	1/4	1/3
2						5.90
4			3.95	4.35	5.87	6.25
6		3.12	3.8	4.35		
8	2.52	3.4	4.35			

Table V. Effective Protector Ambients in Degree Centigrade of Protectors for 115 Degree Centigrade Ultimate Trip

Number Poles	Horsepower 1/20	1/12	1/8	1/6	1/4	1/3
2						93.5
4			92.5	94	95.5	94.5
6		87	88	89		
8	825	83	82.5			

Table VI. Locked Rotor Test

Type of Motor, Open Split Phase—Horsepower, 1/6—Number Poles, 4
Type of Test, Locked Rotor—Volts, 115—Amperes, 17.8–15.6—Ambient, 25 Degrees Centigrade

			Temperatures by Thermocouple, Degrees Centigrade					
Amperes	Volts	Time	Main Winding Switch End	Start Winding	Iron	Protector Air	Protector Cap.	Ambient
0	115	0	25	25	25	25	25	25
17.8	115	3		40				
17.4	115	5		60				
16.5	115	9		80				
16.1	115	12		100				
(Off) 15.6	115	16		120				
Override			55	125	35	32	26	25

will heat the winding to the selected limiting temperature and protector ambient. From the curves developed in Figure 10, we can read the ultimate trip current for 115 degrees centigrade for a given wire size. We must now develop a means of determining the protector ambient temperature.

Protector Ambient Temperature

This temperature is very difficult to measure directly as part of the protector is in contact with the motor frame which is at one temperature and another part of the protector extends out into the air within the motor frame where it is subjected to both the air temperature and radiation from the winding.

By standardizing protector test methods it has been possible to simulate average conditions and get reproducible protector ultimate trip curves.

To correlate the protector data with motor test data, the previously mentioned line of motors was run with protectors and by working backwards as in Figure 13, the effective protector ambient is obtained. Curve A–C is a heat run curve on a motor. Protector number *10* produced an ultimate trip temperature of 115 degrees centigrade at 4.15 amperes. The ultimate trip curve of protector number *10* crosses the ordinate for 4.15 amperes at 94 degrees centigrade, which we can assume is the effective protector ambient temperature for 115 degrees centigrade winding temperature and 4.15 amperes. When developing this information it will not be easy to get protectors which will give exactly 115 degrees centigrade ultimate trip temperatures. In these cases it is possible with a little experience to interpolate with a sufficient degree of accuracy.

The values of effective protector ambients listed in Table V were taken from the ultimate trip curves of the protectors used to get an ultimate trip of 115 degrees centigrade for each of the motors in the line. These values were plotted along with horsepower and inches of iron to produce the series of parallel curves in Figure 14. The curves are plotted for a uniform spacing even though the actual test results here did not produce as smooth a set of curves as the winding temperature data. It is assumed that these variations are due to inability to get a higher degree of testing accuracy.

Selecting a Protector for Ultimate Trip

We now have sufficient information to select a protector to give us an ultimate trip of 115 degrees centigrade for a motor of similar mechanical design to the line we have tested. To illustrate, we will assume that we have from our motor design information a 1/6 horsepower, 1,750 rpm motor with a main winding of 2,025 circular mils and 2 inches of iron.

From the 4-pole curve in Figure 10 we find that 5.1 amperes will produce a 115 degrees centigrade winding temperature, and in Figure 14 we find that a 1/6 horsepower motor with 2 inches of iron will give an effective protector ambient temperature of 88 degrees centigrade.

The nearest ultimate trip curve (number *9*) in Figure 11 to the intersection of the 5.1 amperes ordinate and the 88 degrees centigrade abscissa will be the protector that will produce approximately 115 degrees centigrade winding temperature at ultimate trip in this 4-pole motor with 2,025 circular mils in the main winding and 2 inches stacking of iron.

While we have previously touched on the question of locked rotor temperatures, we have made the protector selection based on running performance. Obviously this has been done because a motor is built to run and, therefore, running conditions should be the most exacting requirements for the protector. On the other hand locked rotor produces the most

Figure 10. Line of split phase long-hour open motors, 60-cycle

Wire size (circular mils) versus amperes to heat main winding to 115 degrees centigrade in 26 degrees centigrade room

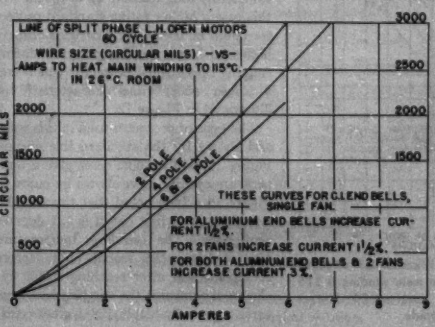
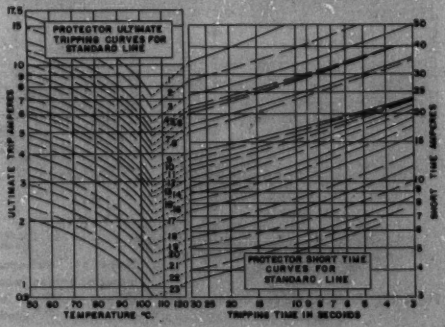


Figure 11. Protector ultimate tripping curves for standard line and protector short time curves for standard line



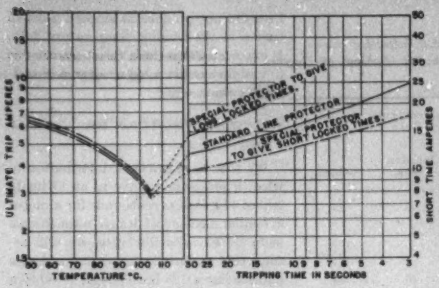


Figure 12. Range of short time trips or approximate ultimate trip conditions

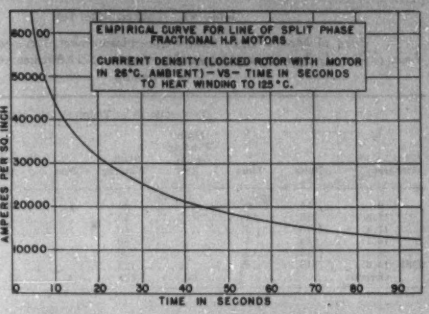


Figure 15. Empirical curve for line of split phase fractional horsepower motors

destructive temperatures so that a satisfactory protector must protect the winding in case the motor becomes stalled.

Locked Rotor

The most difficult period for protecting a stalled motor is when the motor is cold for then the protector must function entirely from the heat produced by passage of the stalled current through the protector. During this very short period the mass of the motor and its radiating area has little or no effect on rate of heating. This leaves us with the heating rate as a function of the current density.

We must, therefore, investigate the highest current density winding of the motor which in most single-phase motors is the phase or start winding.

In Figure 6 we have the time-temperature curve for a winding operating at 33,500 amperes per square inch. If the current density had been greater or less than existed in the test that produced this curve, then the time to reach 125 degrees centigrade would have been less than or more than the 17 seconds shown in Figure 6. Stalled rotor tests similar to that plotted in Figure 6 were run on many motors, not only the line mentioned previously in this paper but other motors with very high and very low current densities. By taking the time required to reach 125 degrees centigrade from each of these curves and plotting them against their respective current densities, we get the curve in Figure 15. From our motor design information we know the current density (amperes per square inch) of the windings for the motor under consideration. From curve Figure 15 we find that the 33,500 amperes per square inch require 17.4 seconds to heat the winding to 125 degrees centigrade.

We have selected previously protector number 9 for running protection of this motor. From our design information we have a locked rotor current of 17.2 amperes. Applying this locked rotor current of 17.2 amperes to the short time curve of protector number 9, right side of Figure 11, we find that the tripping time of this protector in a 25 degrees centigrade room is ten seconds. This time is less than the time required for this same value of locked rotor current to heat the winding to 125 degrees centigrade and, therefore, we can expect the first tripping of the protector to prevent the winding temperature from reaching 125 degrees centigrade.

If the selected protector would have allowed overheating of the winding, then a lower standard rating could be used resulting in lower ultimate trip temperatures or a special protector as illustrated in Figure 12 could be used.

In some few cases the selected standard protector might not allow a particular motor, when starting, to stay on the line sufficiently long to come up to speed. In this case a higher rating standard protector could be used with a correspondingly higher ultimate trip temperature during running or a special protector which had a longer short time characteristic as illustrated in Figure 12.

The type of protector used in this work and shown by the standard line curves, Figure 11, will in most cases give a temperature cycling curve shown by curve in Figure 16. It is noted that the winding temperature at first cutoff is 121 degrees centigrade. The winding temperature after a few cycles reaches 139 degrees centigrade and after reaching stable cycling temperatures the winding temperatures repeatedly attain 128 degrees centi-

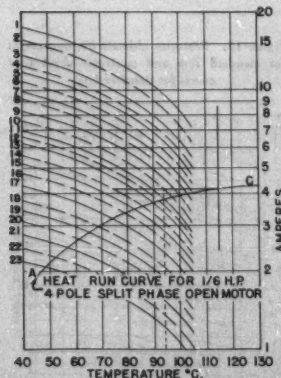


Figure 13. Protector ultimate tripping curves for standard line

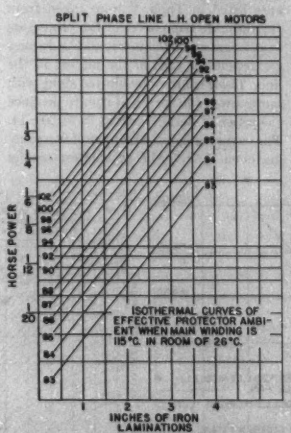


Figure 14. Isothermal curves of effective protector ambient when main winding is 115 degrees centigrade in room of 26 degrees centigrade

Table VII. Heat Run Test

Type of Motor, Open Split Phase—Horsepower, 1/6—Number Poles, 4
Type of Test, Heat Run—Volts, 115—Amperes, 2.6–4.0–4.45—Ambient, 27 Degrees Centigrade

			Temperatures by Thermocouple Degree Centigrade						
Amperes	Volts	Time	Main Winding Switch End	Start Winding	Iron	Protector Air	Protector Cap.	Main Winding Fan End	Ambient
0	115	0	27	27	27	27	27	27	27
2.6	115	90	61	58	59	58	50	55	27
2.6	115	110	61	58	59	58	50	55	27
2.6	115	120	61	58	59	58	50	55	27
Load Raised to 4.0 Amperes at 120 Minutes									
4.0	115	310	106	100	101	97	77	92	27
4.0	115	330	106	100	101	97	77	92	27
4.0	115	340	106	100	101	97	77	92	27
Load Raised to 4.35 Amperes at 340 Minutes									
4.35	115	480	134	127	127	122	92	117	27
4.35	115	500	134	127	127	122	92	117	27
4.35	115	510	134	127	127	122	92	117	27

grade cutoff temperatures. This is acceptable as it is less than the Underwriters' allowable temperature of 150 degrees centigrade peak temperature.

Discussion

All data and curves used in this paper are based on actual test data and design information. Tables VI and VII were prepared in order to show the type of test.

No doubt there are other methods of approach to this problem of selecting the proper protector for a motor during its design. It is hoped that this paper will stimulate thinking along this line and result in simpler and better ways of accomplishing the desired results.

This work has indicated that more accurate temperature testing is required than has been common practice. In the higher temperatures where the heat curve flattens out Figure 4, it is necessary to get very accurate current readings. It is desirable to make these tests in constant temperature rooms and with accurate controls of voltage and load currents.

The least consistent test results obtained in this study are the protector ambient temperatures from which curves in Figure 14 were plotted. Fortunately, at the point where the ultimate trip curves are used the errors encountered have a minimum effect on the motor winding temperature. No doubt continued work in this field will refine this information or will bring forth a new and better method of arriving at the proper effective protector ambient information.

Conclusions

Where sufficient ground work has been done it is possible to preselect protectors from physical design characteristics of motors with a reasonable degree of accuracy. Expanded use of protected motors will result (in time) in a new method of rating motors.

The author recommends that:

1. Motor engineers attempt to develop simpler methods of protector selection.

2. The AIEE committee on electrical insulation develop curves to replace those shown in Figures 1 and 2.

3. The AIEE adopt standard locations and methods of applying thermocouples to motors.

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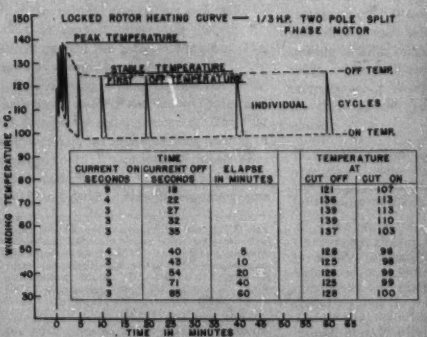

CURRENT ON SECONDS	TIME CURRENT OFF SECONDS	ELAPSE IN MINUTES	TEMPERATURE AT CUT OFF	TEMPERATURE AT CUT ON
9	18		121	107
4	22		136	113
3	27		139	113
3	32		139	110
3	35		137	103
4	40	5	128	98
3	43	10	125	98
3	54	20	128	99
3	71	40	125	99
3	95	60	128	100

Figure 16. Locked rotor heating curve 1/3 horsepower 2-pole split phase motor

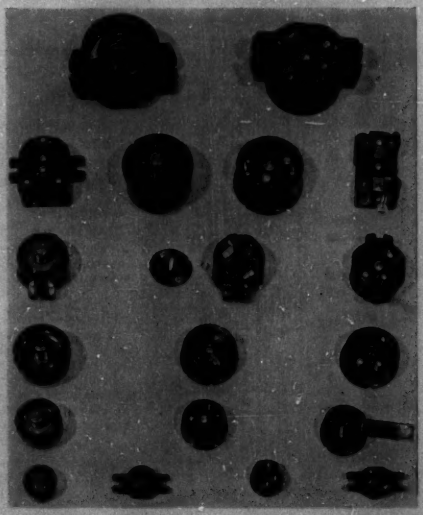

Figure 17 (right). An exhibit of available disk-type of inherent overheat motor protectors showing physical variations

Discussion

L. C. Schaefer and C. G. Veinott (Westinghouse Electric Corporation, Lima, Ohio): The author is to be commended for his studies made in an attempt to cut the time required for the selection and application of inherent overheating protectors to motors. Anyone who has faced the problem of making such applications to literally hundreds of different motor designs appreciates the need for short cuts.

We were a little surprised that Vaughan included in his definition of inherent-overheat protector a device responsive only to temperature. In our practice, we have considered that inherent-overheating protection was provided only by a device responsive both to motor temperature and to the current traversing the motor, as described in Gano's fundamental patent (United States Patent Number 1,693,379). In practice, the disk-type thermostat requires an auxiliary heater traversed by current in order to obtain locked-rotor and running protection. Temperature-sensitive thermostats are used on integral horsepower motors, but these afford only running protection; locked-rotor protection is obtained by the use of an external current-operated device.

Vaughan mentions as the first commercial application of a disk-type thermostat its use on a hermetic refrigerator. This application pioneered in two important respects.

1. The thermal protective device gave true inherent overheating protection because the device was responsive to both temperature and current.
2. The device automatically reset itself.

At that time, the usual practice in domestic refrigerators was to use a manual-resetting current-operated device located inside the refrigerator. Now, the trend towards the use of dome-mounted automatic-reset protective devices is well established, a tribute to the sound engineering pioneered in 1929.

While discussing the history of built-in thermal protection, we should point out that it was used commercially in a limited number of electric drills in 1928. Built-in overload protection was introduced on fractional horsepower motors about 1931.

Vaughan's method undoubtedly gave satisfactory results for the line of motors tested. How practicable his approach would be for other lines of motors needs to be established. Fundamentally, the ideal application would be one in which the temperature of the bimetal disk followed closely the hot-spot temperature of the winding under all conditions of load, ambient, and so forth. With this kind of an application the disk would be set to operate at the desired hot-spot temperature of the winding with full assurance that complete protection would be afforded against all conditions of load, ambient temperature, line voltage and ventilation. We believe an ideal selection method should take into account directly the opening temperature of the disk, whereas Vaughan used it only indirectly. Moreover, it appears to us to be a rather artificial approach to determine the protector ambient temperature by use of curves; why not measure it directly and eliminate any errors due to errors in the ultimate tripping curves of the thermostats? Our experience with this type of approach has given inconsistent results. The author confesses the same difficulty when he reports that the curves in Figure 15 "are plotted for uniform spacing even though the actual test results here did not produce as smooth a set of curves as the winding temperature data." These kinds of tests are difficult to take with consistent results and the author is to be commended for his honesty.

V. G. Vaughan: I offer the following as a rebuttal of the discussion offered by Schaefer and Veinott.

The major part of this discussion seems to be a question of effective ambient temperature of the protector.

The selection and application of inherent protectors would be very simple if there was any temperature that could be measured that would give this effective protector ambient correctly. In our work we have found that even in the same line of motors changing from one type of material in the end bell to another, or a different speed or changing the ventilation, changes the protector ambient temperature so much that there are no consistent values that can be obtained by measurement or calculation, and that is the reason we have resorted to the empirical method of running tests and arriving at the values to be used as a result of numerous tests on a given line.

I do not want to imply that the method used here is the only good one. As a matter of fact as indicated in the paper it is my hope that this paper will stimulate other people to make investigations which will result in their adopting a standard method of selecting protectors with less work than is now required by this cut and try method.

Mercury-Arc Power Converters in North America

AN AIEE COMMITTEE REPORT

Part I. Introduction

THIS is a report on the present status of the design and application of mercury-arc power converters on the North American continent. It was prepared because there was a need of a comprehensive review of this subject, for the benefit of those who have not kept up with the voluminous literature. A selected bibliography is given at the end of the paper, and numerical references are made to it in the text.

The operation of a mercury-arc power converter is based on the valve action of a mercury arc in vacuum, which permits currents to flow in one direction only—from the anode to the mercury-pool cathode.

The mercury-arc rectifier was developed in the United States by Cooper-Hewitt, following his introduction of the mercury-arc lamps in 1901. The first commercial application was made in 1904 to convert alternating current to direct current for battery charging. The rectifier was of the glass-bulb type.

The development of metal-tank rectifiers was started in the United States and in Europe several years later. The first commercial installation was made in Europe in 1911, and several installations were made in the United States between 1913 and 1915. This development was then suspended in the United States. It was resumed about ten years later, following the introduction of metal-tank rectifiers of European make in 1924. The application of glass-bulb rectifiers was continued during the intervening period.

The use of metal-tank rectifiers in the United States and Canada, for converting alternating-current to direct-current, has increased rapidly since 1925. Their aggregate capacity is now about 5,500,000 kilowatts. Most of the earlier applications were for electric railway service. During the last ten years the electrochemical applications have become the largest. Rectifiers aided in the large-scale expansion of aluminum and magnesium production during the war. Other important applications are for industrial service and mining.

Mercury-arc rectifiers have practically superseded rotating-type a-c to d-c converters for d-c voltages over 200 volts and ratings over 150 kw, except for rolling mill drives and other applications requiring a wide range of voltage adjustment. The following characteristics of mercury-arc converters are responsible for their ascendency over rotating machines:

1. They are static equipment, are quiet in operation, require no special foundation, and can be installed in locations unsuitable for rotating machines.

2. They can be switched on and off like a transformer, require no synchronizing and no polarity checks, and are adaptable for automatic or remote control. They can operate on any commercial power system frequency and are relatively unaffected by a-c system disturbances. Faults can be cleared quickly without damage and with only a short service interruption.

3. They have a relatively high efficiency over the greater part of the load range, and their idling losses are low.

4. The operating and maintenance costs are generally lower than for rotating machines.

While the chief application of mercury-arc converters has been for converting a-c power to direct current, they can be used also for converting d-c power to alternating current, or as frequency changers. Two noteworthy frequency-changer installations were made about five years ago in steel mills, to provide a flexible nonsynchronous tie between the 60-cycle and 25-cycle power systems for the transfer of power between them.

Until about ten years ago all mercury-arc rectifiers were of the multianode type, with 6, 12 and 18 anodes per tank. The trend had been to increase the number of anodes per tank to obtain a higher current rating per rectifier. This trend was reversed by the introduction of the single-anode type ignitron, followed several years later by the excitron. Single-anode tanks or tubes, each containing one main anode and a cathode, are assembled in groups of six and twelve to make up a unit.

The single-anode type rectifier has largely superseded the multianode type in the United States, for the following reasons:

1. Lower arc drop and consequently higher efficiency.

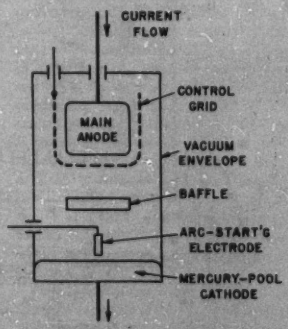

Figure 1. Elementary single-anode, pool-cathode mercury-arc rectifier tube

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This report was prepared by a working group of the AIEE electronic power converter committee which included H. Winograd (chairman), Allis-Chalmers Manufacturing Company, Milwaukee, Wis.; C. R. Marcum, Westinghouse Electric Corporation, East Pittsburgh, Pa.; L. W. Morton and H. C. Steiner, both with the General Electric Company, Schenectady, N. Y.

2. Simpler maintenance, because individual tanks or tubes can be removed and replaced without affecting the others of the group.

3. More adaptable for quantity manufacture, because a larger number of smaller duplicate parts are involved.

4. Fewer sizes are needed, because higher ratings can be obtained by increasing the number of tubes per group.

Single-anode mercury-arc rectifiers are made in the pumped-tube and sealed-tube construction, and several types are described in Part II of this paper. Sealed tubes are generally applied for rectifier units of lower rating, while pumped rectifiers are usually applied for units of higher rating and for heavy-duty service. While there is no rigid dividing line, most of the existing sealed tube installations have unit ratings of 1,000 kw or less. However, there is an increasing trend towards the application of sealed-tube rectifiers for higher ratings.

Part II. Construction of Rectifiers

Basic Tube

The basic elements of a single-anode rectifier tube are shown in Figure 1. The main anode, the mercury-pool cathode, and the auxiliary electrodes are enclosed in a vacuum envelope. The electrical connections to the main anode and the

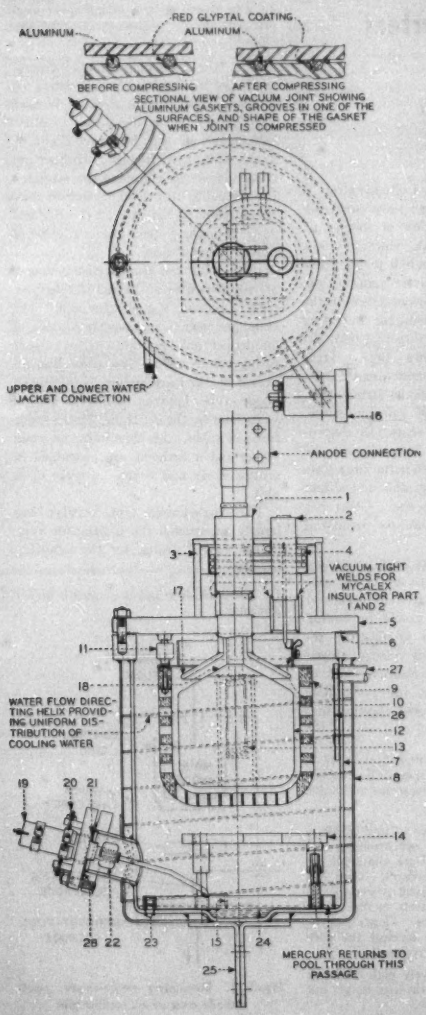
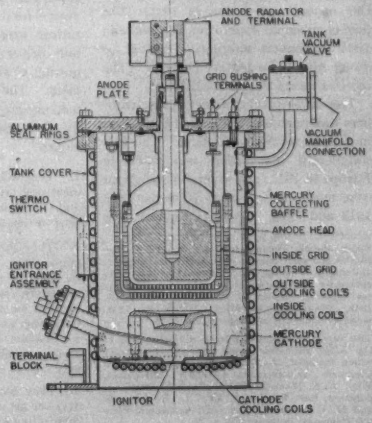


Figure 2 (left). Cross section of pumped Ignitron tube rated at 667 amperes. Diameter of vacuum tank 16 inches (40.6 centimeters)

1. Mycalex anode insulator
2. Mycalex insulator (lead to grid)
3. Anode heater cover
4. Anode heater
5. Anode cover
6. Two (2) aluminum gaskets
7. Vacuum envelope
8. Water jacket
9. Grid
10. Support ring for part 9
11. Insulator for part 9 (mycalex)
12. Graphite anode
13. Anode stud
14. Mercury splash baffle
15. Ignitor tip
16. Vacuum valve
17. Heat shield
18. Anode spacer
19. Mycalex insulator for ignitor and excitation anode leads
20. Adjusting screws for part 15
21. Flexible diaphragm (and adjusting screws part 20) permit adjustment of ignitor immersion (in mercury pool) to correct value from outside tank
22. Excitation anode
23. Mercury separators
24. Mercury pool (cathode)
25. Cathode connection
26. Exhaust baffle
27. Exhaust pipe
28. Gaskets (inner formvar, outer aluminum)

Figure 3 (below). Cross section of pumped Ignitron tube for 3,000 volts d-c service

A 6-tube rectifier is rated at 3,000 kw. Diameter of vacuum tank 15 inches (38.1 centimeters)



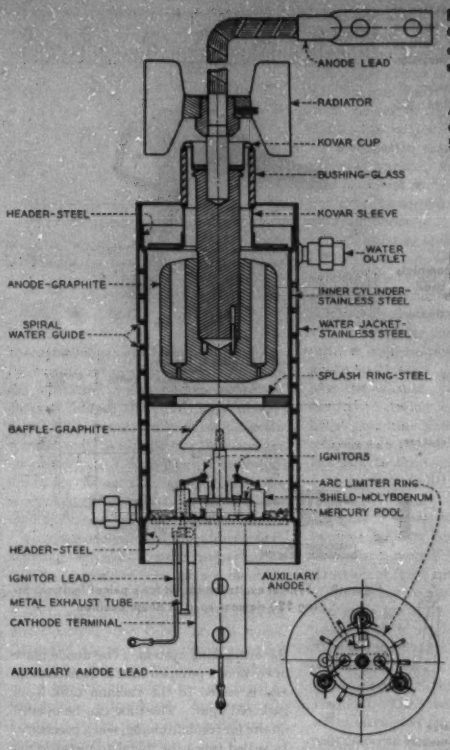
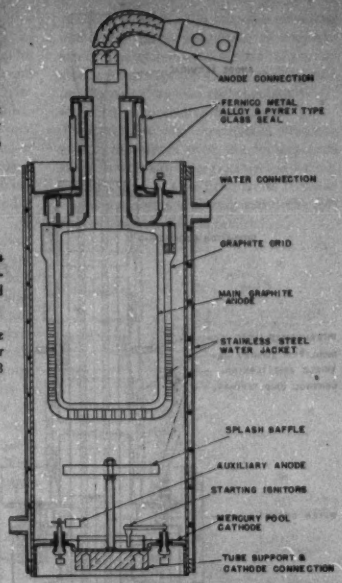


Figure 4 (left). Cross section of 200-ampere sealed ignitron tube

Approximate outside diameter $5^3/_4$ inches (14.6 centimeters)

Figure 5. Cross section of 400-ampere sealed ignitron tube

Approximate outside diameter nine inches (23 centimeters)



auxiliary electrodes are made through vacuum-tight insulating bushings in the envelope. The cathode connection is usually made on the envelope. The flow of current consists of a stream of electrons from the cathode to the anode. By convention, the direction of current flow is opposite to the direction of electron flow. Electrons are emitted at cathode spots on the mercury surface, which can be initiated by the arc-starting (ignition) electrode.

The flow of current can start only when there is a cathode spot on the mercury, when the anode is positive with respect to the cathode, and when the potential of the control grid (if present) is such that the electrons are permitted to reach the anode. The flow of electrons ionizes the mercury vapor in the arc path, and positive ions of mercury neutralize the negative space charge of the electrons. Large currents can flow with a relatively low voltage drop in the arc, of the order of 15 to 25 volts at rated current, depending on the rectifier design. The magnitude of the current is determined by the circuits to which the rectifier is connected. The control grid has no influence after the flow of current has started and can regain control only after the current flow has stopped.

Baffles are placed in the arc path to shield the anode from the mercury vapor and droplets evolved at the cathode or to provide deionizing surfaces. Rectifier tubes are cooled by water circulated through a water jacket over the vacuum envelope or through a cooling coil, to remove the arc losses and control the mercury vapor pressure. Air-cooled rectifiers have also been used. The mercury vapor condenses on the cooled surfaces and returns to the cathode. The main anode, grid, and baffles are usually made of graphite. The vacuum envelope is made of steel.

Rectifiers are subjected to a degassing process, as part of the manufacturing procedure, to remove occluded gases from the internal parts. These parts are heated to or above their operating temperature, by passing current through the rectifier or by other means, and the liberated gases are removed by vacuum pumps. The vacuum envelopes of sealed tubes are also heated to degas their inside walls.

Types of Tubes

There are two general types of single-anode mercury-arc rectifiers: the ignitron and the excitron, which differ in the method of cathode excitation. In the ignitron the cathode spot is initiated during each cycle of the a-c voltage by means of an ignitor of refractory material immersed in the mercury. The start of the conducting period in the cycle is controlled by timing of the ignition, and there is no arc at the cathode during the non-conducting period of the cycle. In an excitron a cathode spot is maintained continuously, while in operation, by means of a pilot arc from an excitation anode. The start of the conducting period of the main anode is controlled by a control grid.

Pool-cathode tubes are made in two types of construction, the pumped tube and the sealed tube. Pumped rectifiers are provided with vacuum pumps, and the tubes are connected continually to

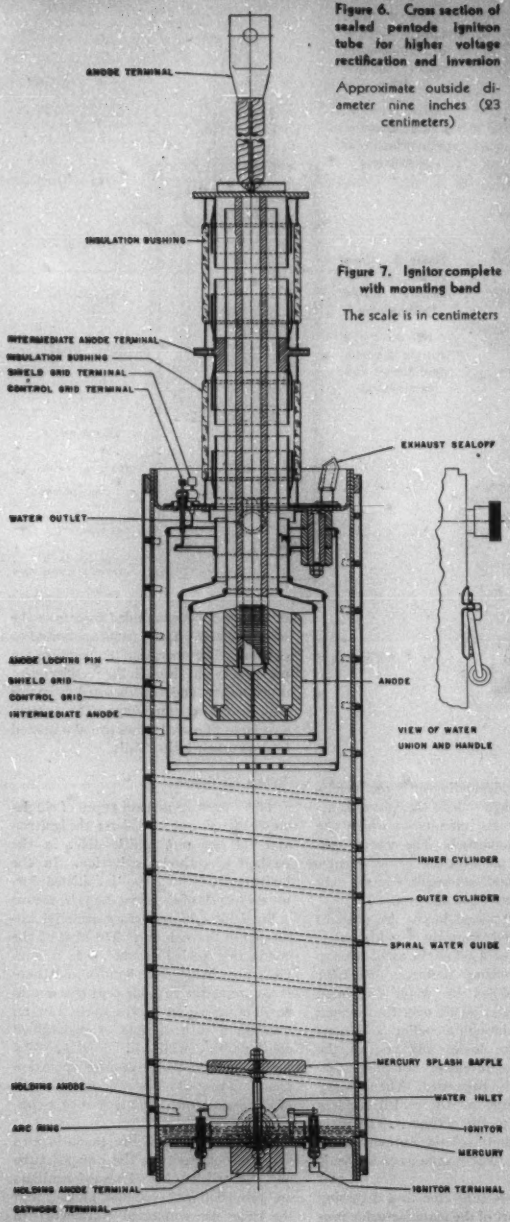


Figure 6. Cross section of sealed pentode ignitron tube for higher voltage rectification and inversion

Approximate outside diameter nine inches (23 centimeters)

Figure 7. Ignitor complete with mounting band

The scale is in centimeters

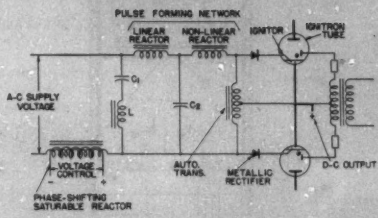


Figure 8. Typical excitation circuit for a pair of ignitrons firing 180 degrees apart in phase position

the evacuating system. The anode plate or cover of a pumped tube is demountable and is sealed to the vacuum tank by a gasketed joint. The tube can be opened on site for reconditioning, when necessary. In sealed tubes the metal-to-metal joints are welded, and the joints of insulating bushings are made by fusing the insulator to metal. The evacuating connection is sealed after the degassing process. The parts of the tube which are in contact with the cooling water, including the water jacket, are made of stainless steel, in order to minimize corrosion and the seepage of hydrogen ions into the tube.

Tube Ratings

The current and voltage ratings of a mercury-arc pool-cathode tube are determined largely by thermal limits and arc-back frequency. An arc-back is the failure of the rectifying action, due to the formation of a cathode spot on the anode when it is negative with respect to the cathode, which results in a flow of current in the reverse direction, from cathode to anode. For low d-c operating voltages, of about 300 volts or less, arc-backs are rare, and the current rating is established generally by the safe operating temperature of some of the internal parts and the

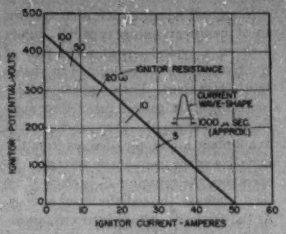


Figure 9. Output characteristics and current wave shape for ignitor excitation circuit of Figure 8

anode bushing. For higher d-c voltages, the current rating of a tube is reduced as the operating voltage is increased, in order to reduce the probability of arc-back. The average arc-back frequency on rectifier units is usually less than one per anode per year. With proper protection, arc-backs cause no damage, and the equipment is returned to service immediately.

Single-anode pumped tubes are made in a number of sizes, for d-c ratings up to 1,000 amperes per tube, at a d-c output voltage of 250 volts. Sealed tubes are made in several ratings up to 400 amperes direct-current per tube, at 250 volts. Generally, the same tube can be used for a range of d-c voltages up to about 900 volts, the current rating being reduced as the voltage is increased. However, the kilowatt rating per tube is increased as the voltage is increased.

Pumped and sealed mercury-pool tubes are made also for higher d-c operating voltages and are described in the following.

Ignitrons

The operation of an ignitron is based on the ability to initiate a cathode spot on a mercury pool, at a precise and controllable instant during each cycle, by passing a pulse of current through an ignitor partly immersed in the mercury. This development was announced by Slepian and Ludwig in 1933.[3] The ignitor, shown in Figure 7, is a pencil-like rod made of a refractory material such as boron carbide. Several types of pumped and sealed ignitrons are described herein.

Pumped Ignitrons

In Figure 2 is shown a cross section of a typical pumped ignitron tube for operation in the range of 250 to 900 volts direct-current. The vacuum tank is cooled by water circulated through a water jacket. The anode is made of graphite and is surrounded by a graphite grid. The ignitor is mounted at the end of a long rod which is connected to an inlet in the side of the tank. The immersion of the ignitor in the mercury pool can be adjusted externally by means of adjusting screws. An auxiliary anode is used for maintaining cathode-spot excitation over the normal conducting period of the main anode.

The insulating seals for the connections to the main anode, grid, ignitor, and auxiliary anode are constructed of "Mycalex," a fused mixture of glass and mica bonded to special steels. A heater is provided around the anode and grid seals to prevent condensation of mercury on the anode assembly. A baffle is mounted over the exhaust opening in the tank to keep the mercury condensed in the exhaust pipe from striking the anode when returning to the tank. The anode plate is sealed to the vacuum tank by aluminum gaskets tightly clamped between flanges.

Figure 3 shows the construction of a pumped ignitron for operation at 3,000 volts direct-current.[10] The general construction of this ignitron is similar to that of the lower-voltage types except that two grids are used. The vacuum-tight anode seal is made of an alloy steel "Kovar" fused to its matching glass. The seal is enclosed by a porcelain bushing which serves as a support for the anode assembly. The vacuum tank is cooled by water circulated through copper cooling coils soldered to its surface. A steel cooling coil is provided inside the tank, with its terminals brought through welded joints in the tank. The internal and external coils are connected in series. A graphite baffle, mounted on insulating supports, is placed above the mercury-pool cathode.

Sealed Ignitrons

Figure 4 shows the general construction of the 100-ampere and 200-ampere sealed ignitron tubes.[11] The vacuum envelope and water jacket are made of stainless steel. The glass-to-metal seals[5] of the insulating bushings are made with "Kovar" or "Fernico," an iron-nickel-cobalt alloy, fused to a matching borosilicate glass. The expansion characteristics of the glass and metal match over the temperature range to which the seal is subjected during manufacture, so that little stress remains in the completed seal and nearly the full strength of the glass is realized.

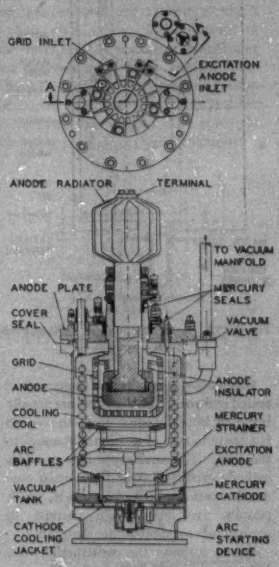
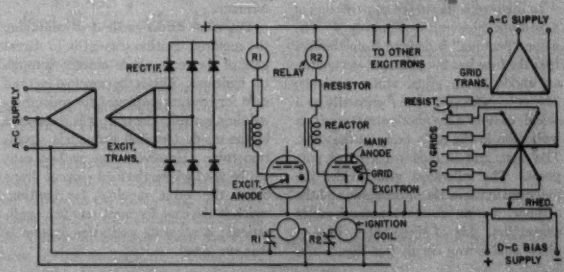


Figure 10. Cross section of pumped excitron rated at 500 amperes

Diameter of vacuum tank 16 inches (40.6 centimeters)

Figure 11. Excitation circuit for a 6-tube pumped excitron rectifier



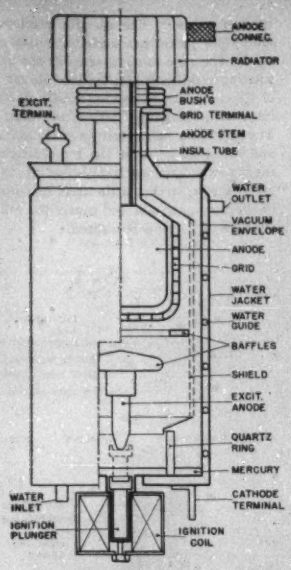


Figure 12. Cross section of 200-ampere sealed excitron

Grids are not required in these sizes of sealed ignitrons. Two ignitors are usually installed, but only one is used; the second is a spare in case of accidental damage to the first. The auxiliary anode provides cathode excitation over the normal conducting period of the main anode, which permits the main-anode current to approach zero without arc instability.

Figure 5 shows the construction of a 400-ampere sealed ignitron.[40] The general construction and the materials used are the same as for the smaller tubes, shown in Figure 4. With the increased physical dimensions, however, a grid is required to control the effects of residual ionization following the conducting period and to attain the desired tube rating.

In Figure 6 is shown the construction of a pentode high-voltage ignitron.[23] The name "pentode" is derived from the fact that the tube has five principal electrodes: an anode, three grids, and a cathode. The outermost grid serves primarily as a shield, to isolate the anode structure from ionization in the main body of the tube. The next grid is used for control purposes. The innermost grid divides the potential between the main anode and the cathode, by an external potentiometer connection. Six of these tubes, in a 6-phase double-way circuit (see Figure 37), have a capacity of 7,500 kw at a d-c operating voltage of 17.6 kv, as a power rectifier or a power inverter.

Ignitron Excitation

Figure 7 shows an ignitor complete with its supporting band for use in pumped or sealed ignitrons. The ignitor point is immersed in mercury to approximately half the length of its tapered section. Mercury does not wet the ignitor material, and a negative meniscus is formed in the mercury at its junction with the ignitor. Cathode spots are formed at this junction when a pulse of current, of the order of 10 to 20 amperes, is passed from the ignitor into the mercury. Ignition of the cathode spots is caused by the thermal and potential gradients produced by the current. When the tube starts conducting and the cathode current increases, the initial cathode spots divide and form new spots. The number of spots depends on the current, each spot providing emission for approximately 15 to 20 amperes of cathode current. The active cathode area on the mercury surface assumes a circular form, with the ignitor at the center, and with a diameter proportional to the current. There is insufficient time for the cathode spots to reach the tube wall during the normal conducting period, and it is unnecessary to insulate the mercury pool from the wall.

The firing characteristics of an ignitor[45,46] are influenced by a number of factors such as immersion depth, ignitor temperature, rate of rise of the ignitor current, as well as by the ignitor design. The effective ignitor resistance decreases with deeper immersion or increased temperature. Variation of immersion depth is due to wave motion on the mercury surface. For a lower resistance, a lower voltage and a higher current are required for ignition. Firing time depends on the rate of current rise. While these factors are complex, from the practical standpoint it is only necessary to define the highest required values of voltages, current, and time to insure satisfactory performance.

The most direct method of supplying the required ignition current is to divert part of the main-anode current through the ignitrons. This is the principal method used for ignitors employed for resistance-welding control; the ignitor is connected to the anode terminal through a thyratron tube. However, this method cannot be used where the load current drops below the value required for ignition, which is the case for most rectifier applications; a separate excitation system is then employed.

Nearly all separate excitation systems are of the energy-storage type.[16,17] Figure 8 shows a typical circuit for a pair of ignitrons, which are connected to phases 180 degrees apart. For a 6-phase rectifier consisting of six ignitron tubes, three such circuits are required. They are connected to a 3-phase a-c supply, derived from the same line as the main-anode voltages. In operation, the capacitor C_3 is charged by the a-c voltage, through the linear reactor. As the capacitor voltage increases, an increasing current flows through the nonlinear reactor. When the voltage across C_3 rises to a predetermined value, the core of the nonlinear reactor becomes saturated, so that its reactance drops to a very low value, and the energy stored in C_3 is discharged through the ignitor to start the cathode spot. This occurs alternately in the two ignitrons during successive half-cycles of the a-c voltage.

The autotransformer provides a return circuit for the capacitor current. The metallic rectifiers prevent the flow of reverse current through the ignitors, which could damage them. The phase or time position of the ignition pulse with respect to the main-anode voltage is obtained by varying the direct current in the control winding of the phase-shifting saturable reactor. The auxiliary anodes of the ignitrons are connected through resistors to an a-c voltage obtained from a transformer, as shown at the right of Figure 8.

Figure 9 shows the output volt-ampere characteristic of the ignitor-firing circuit in Figure 8, and the pulse-current wave shape. The point on the characteristic at which ignition occurs is determined by the ignitor resistance, as indicated by the resistance lines.

Excitrons

Pumped Excitron

A cross-section view of a pumped excitron tube is shown in Figure 10.[28] The main anode is supported by a steel stem, which is brought through a ceramic insulator on the anode plate with gasketed vacuum-tight seals covered by mercury. The anode is enclosed by a perforated control grid. The mercury pool of the cathode on the bottom of the vacuum tank is enclosed by a quartz ring.

The arc space of the tube is surrounded by a helical cooling coil made of steel tubing, which is insulated from the tank and is supported by insulating seals on the anode plate through which the coil terminals are brought out. The coil serves a dual purpose: it is used for cooling the rectifier and condensing mercury vapor; it also shields the tank to prevent

transfer of the cathode spot from the mercury to the tank wall. The turns of the coil are separated by narrow gaps, which permit passage of mercury vapor and gases but prevent arc-through. The gases are exhausted by the vacuum-pumping system to which the tank is connected through a bellows-type vacuum valve. The cooling water is circulated through the cathode water jacket and the cooling coil in series.

The excitation anode, used for starting and maintaining a cathode spot, is supported by the lower baffle and insulated from it. It is connected to an insulated lead-in on the anode plate by a wire passing through a ceramic tube. The excitation anode is connected to the cathode through an external circuit, which is provided with a source of direct current, as shown in Figure 11. The arc-starting device consists of a nozzle-shaped plunger in a well at the bottom of the tank below the mercury pool, and an ignition coil over the well. When the coil is energized, the plunger is pulled down against a spring and ejects a jet of mercury which short-circuits the excitation anode to the cathode and closes the excitation circuit. When the jet is broken an arc is started with a cathode spot on the mercury surface. The arc is maintained as long as the excitation circuit remains energized.

The main and excitation anodes, the grid, and baffles are made of graphite. The other internal parts, except the insulators, are made of steel. The tank supports serve as the external cathode connection. The anode connection is made at the top of the anode stem.

Excitation Circuit

The excitation and grid-control circuits of a 6-tube excitron rectifier are shown in Figure 11. The d-c excitation supply is obtained from a 3-phase a-c source through a transformer and a selenium rectifier. The excitation anode of each excitron is connected to the positive terminal of the d-c supply through a reactor, resistor, and a relay. When voltage is applied to the excitation transformer, the ignition coils are energized and start the excitation arcs. The excitation current operates the relays (*R1*, *R2*, and so forth) which open the circuits of the ignition coils. The d-c excitation supply is about 35 volts, and the excitation current per tube is about 8 amperes.

The control grids of the excitrons are connected to the 6-phase secondary of a small grid transformer through current limiting resistors. The neutral point of the grid transformer is connected to the cathode terminal, in series with d-c bias voltage. The point in the cycle at which the grid-to-cathode voltage becomes positive, to permit the main anode to fire, can be regulated by varying the bias voltage. The a-c voltages of the grids have a definite phase relation to the voltages of the main anodes. Instead of using a variable bias, the firing points of the main anodes in their voltage cycles can be controlled by shifting the phase position of the grid a-c voltages, by means of a phase shifter ahead of the grid transformer.

Sealed Excitron

A cross-section view of a sealed excitron tube is shown in Figure 12. The vacuum tank and its water jacket are made of stainless steel. The insulating anode bushing consists of a number of circular disks with interposing layers of vitreous enamel, which are fused together into a vacuum tight assembly. The vitreous enamel serves as the insulating medium. The vacuum envelope is shielded from the arc by means of a perforated shield.

The arc-starting device consists of a steel plunger with a cup-shaped cap. It floats in the mercury and makes contact with the excitation anode when there is no excitation current. The ignition coil is connected in series with the excitation circuit, as shown in Figure 13. When a d-c voltage is applied to the excitation circuit, current flows through the ignition coil and the excitation anode to the cathode. The plunger is pulled down by the ignition coil, breaking contact with the excitation anode to establish the excitation arc. As long as the excitation arc is maintained, the plunger is held down in the well, under the mercury pool, by the current in the ignition coil. If the excitation arc should go out, it will restart automatically as long as the excitation-supply voltage is applied to the circuit. The excitation current is about 7 amperes per tube.

Part III. Rectifier Units

Connection Diagram

A diagram of connections of the power circuits of a typical 6-phase rectifier unit is shown in Figure 14. It consists of a rectifier transformer, a rectifier, and switching equipment. The rectifier transformer is used for transforming the a-c line voltage to the voltage and number of phases required for obtaining the desired d-c voltage of suitable wave shape.[1,10] Most of the mercury-arc rectifier units have six or twelve phases. The transformer winding which is connected to the a-c line is called the a-c winding, and is connected usually in delta or wye. The winding connected to the rectifier is called the d-c winding. The d-c winding in Figure 14 has a 6-phase double-wye connection and consists of two Y-connected windings with their neutral points joined by an iron-core reactor called an interphase transformer (I T).[1] The two windings with their rectifying elements operate as two 3-phase rectifier groups in parallel, but produce a d-c voltage with a 6-phase wave shape. The midpoint of the interphase transformer constitutes one d-c terminal of the rectifier unit. This is the most generally used connection for mercury-arc rectifiers. Other rectifier transformer connections are given in references 1 and 37 of the bibliography.

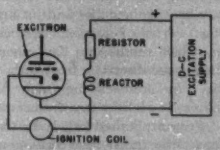


Figure 13. Excitation circuit for sealed excitron tube

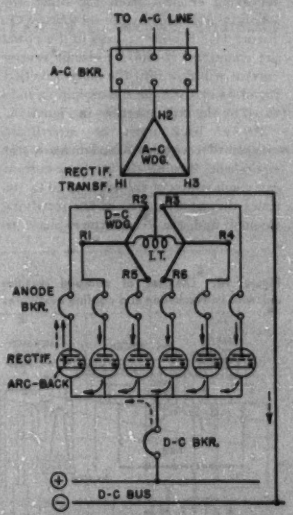


Figure 14. Typical power-circuit diagram of 6-phase rectifier unit

Arrows indicate current flow during an arc-back

The rectifying elements serve as unidirectional switches to connect the phases of the transformer d-c winding to the d-c circuit during a portion of their voltage cycles. In Figure 14, one-half of the total direct current flows through each 3-phase group, and is conducted in sequence through each of the three phases of a group during about one-third of a cycle. The position of the conducting period in the cycle of each phase voltage can be controlled by means of the control grids or by cathode ignition timing (for ignitrons). This control is used for regulating the d-c voltage and is called phase control.

Protection

The circuit breakers shown in Figure 14 are used for connecting the rectifier unit to the a-c and d-c circuits and for protection against overloads and arc-backs. An arc-back is the major fault in a power rectifier. It is a failure of the rectifying action which results in a flow of current in the reverse direction, from cathode to anode. An arc-back imposes a short-circuit on the rectifier transformer and on the d-c circuit, limited largely by the impedance of the transformer phase to which the faulted rectifying element is connected. Current flows to the faulted rectifying element from the other elements, as indicated by the solid arrows in Figure 14. If other sources of d-c power are connected to the d-c circuit, reverse current will flow into the faulted rectifying element from the d-c circuit, as indicated by the broken arrows in Figure 14.

An arc-back must be interrupted quickly to avoid possible damage to the equipment, because the arc-back current can reach values of the order of 50 to 100 times the rated current of the rectifying element and the transformer phase to which it is connected.[20,21] The fault current from the other phases can be interrupted by tripping the a-c circuit breaker. The fault current from the d-c circuit can be interrupted by tripping the d-c circuit breaker. If an anode circuit breaker is used, the fault currents from both sources can be interrupted by tripping the circuit breaker pole in the circuit of the faulted rectifying element, without opening the a-c and d-c circuits. Although anode circuit breakers were used as early as 1931, most rectifier installations prior to 1940 were protected by a-c and d-c circuit breakers. High-speed anode circuit breakers were introduced in 1940, for the protection of rectifiers in an aluminum reduction plant. They have been used for most of the rectifier installations in aluminum and magnesium reduction plants in the United States since then. They have been applied also for other rectifier installations, when better protection and greater operating flexibility is required. Better protection is provided by the anode circuit breakers because they are faster than a-c circuit breakers.[14,22] If an anode circuit breaker is used, the a-c circuit breaker can be eliminated, and protection against a possible fault in the rectifier transformer is provided either by fuses or by some other circuit breaker in the a-c supply circuit. In that case the transformer is connected to the a-c circuit by disconnect switches which are capable of interrupting the transformer exciting current.

When an anode circuit breaker is used, a d-c circuit breaker is usually provided also, for protection against excessive overloads. The anode and d-c circuit breakers in most installations are tripped by reverse current. Sometimes the d-c circuit breaker is provided with an overload trip also. When an anode circuit breaker is not used, the a-c circuit breaker is tripped by a time-delay overload relay for overload protection and by an instantaneous relay with a higher setting for arc-back protection. In installations where the reverse current from the d-c circuit can reach high magnitudes during an arc-back, high-speed anode circuit breakers (if used) or d-c circuit breakers are employed, with an interrupting speed of less than one cycle, to interrupt the circuit before the current attains a high value.

For d-c operating voltages of 750 volts or less, it is the present practice in the United States to use high-speed anode or d-c circuit breakers when the connected capacity on the d-c bus exceeds 2,500 to 3,000 kw. A semi-high-speed d-c circuit breaker is used when the anode circuit breaker is of the high-speed type. For higher d-c voltages, up to 3,000 volts, only high-speed d-c circuit breakers are employed.

The arc-back current to the faulted rectifying element from the other rectifying elements can be suppressed by applying a sustained negative voltage to the control grids (grid blocking) or by interrupting the cathode excitation. The rectifying elements which are not conducting at that time will be prevented from starting. The others will be prevented from restarting after their currents have dropped to zero. The backfeed current from the d-c circuit can be interrupted only by opening the circuit. Arc suppression by grid or excitation control cannot be relied on for full protection and must have back-up protection by circuit breakers, in case it does not suppress the fault current.

Auxiliaries

The tubes of a pumped rectifier unit are mounted on a base and are connected to the evacuating and cooling equipments. These auxiliaries and their connection with the rectifier are shown diagrammatically in Figure 15. Each rectifier tube is

Figure 15. Diagram of pumped rectifier unit with its evacuating and cooling auxiliaries

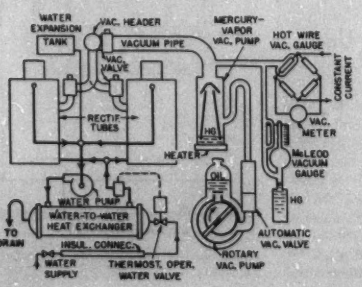
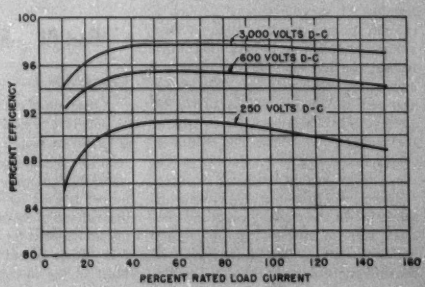


Figure 16. Typical over-all efficiency curves of mercury-arc rectifier units, as a function of the load, for several d-c operating voltages



connected to a vacuum header through an individual valve. The header is connected to two vacuum pumps in tandem, a mercury-vapor pump and an oil-sealed rotary pump. The mercury-vapor pump consists of a cylinder containing a pool of mercury, which is heated by an electric heater. The mercury vapor rises through a nozzle and is deflected downward towards the cooled walls of the cylinder, carrying along the gases which have diffused into the pump from the rectifier tubes. The mercury vapor condenses, while the gases are drawn into the rotary vacuum pump. The rotary pump consists of a cylinder with an eccentrically-mounted rotor and sliding vanes. The gases received from the mercury-vapor pump are compressed by the vanes and discharged to the atmosphere through a check valve and the oil. A vacuum valve between the two vacuum pumps closes automatically when the rotary pump stops, to keep the oil from backing up into the mercury-vapor pump. The mercury-vapor pump is cooled by water or air. The vacuum pipes are also cooled to prevent transfer of mercury between the rectifier tubes and the vacuum pumps.

Two types of vacuum gauges have been used for measuring the vacuum of mercury-arc rectifiers. In the McLeod vacuum gauge, a sample of the gas in a bulb is compressed into a measuring tube by raising the mercury in a well to a preset height in a tube alongside the measuring tube. The difference in the levels of the mercury in the two tubes is a measure of the gas pressure and can be read on a scale calibrated in microns. A micron is the pressure exerted by a column of mercury 0.001 millimeter high.

The operation of the hot-wire vacuum gauge is based on the fact that the thermal conductivity of a gas is a function of its pressure. A wire having a high temperature coefficient of resistivity is enclosed in a tube which is connected to the vacuum system. The wire is connected as part of a Wheatstone bridge and is supplied with a constant heating current. The lower the pressure of the gas the lower is its thermal conductivity, and the higher is the temperature and resistance of the wire. The resistance is therefore a measure of the gas pressure and is indicated by a vacuum meter calibrated in microns. Contacts on the vacuum meter, or a relay in parallel with it, can be used for controlling the vacuum pumps and for protecting the rectifier against operation with a poor vacuum. The normal operating pressure of a rectifier, as measured by the vacuum gauges, is usually below five microns.

Most of the heat generated in a rectifier by the arc-drop losses is carried off by the cooling system. In Figure 15 the cooling water is circulated between the rectifier tubes and a heat exchanger by a motor-driven water pump. A water expansion tank is connected to the recirculating system, to permit expansion of the water when it is hot and to provide a water head on the system for preventing entrance of air. In the heat exchanger, the circulated water is cooled by raw water from a supply, which is discharged to a drain after passing through the tubes of the heat exchanger. The temperature of the recirculated water is regulated by a thermostatically-controlled valve, which controls the flow of raw water. The temperature of the recirculated water is maintained usually at some value between 45 and 55 C. Sometimes heaters are installed in the rectifier cooling circuit to keep the temperature from dropping too low during periods of light load.

A corrosion inhibitor, such as sodium chromate, is usually added to the rectifier cooling system. If the cooling circuit should be exposed to freezing temperatures, an antifreeze solution is added. In installations where a reliable supply of raw water is not available, or the cost of water is too high, a water-to-air heat exchanger can be used. The rectifier water is circulated through a radiator which is cooled by a fan or blower. In many rectifier installations the cooling equipment is insulated from ground and kept at the same potential as the rectifier. The connection to the raw water supply is then made through an insulating pipe, such as a rubber hose, as shown in Figure 15. If the cooling equipment is grounded, insulating water connections are used between this equipment and the rectifier.[8,44]

Sealed-tube rectifiers, of course, do not have any evacuating system. The tubes usually are mounted in a metal-enclosed cubicle of a unit-type substation. Many sealed-tube rectifiers, particularly for lower capacities, are cooled by tap water and may not require any water pumps. For underground mine service, portable substations are generally used, and the equipment is mounted on low-headroom mine cars. The tubes are cooled by a circulating water cooling system with a water-to-air heat exchanger.

Part IV. Rectifier Characteristics

The important quantitative characteristics of mercury-arc rectifier units are

Figure 17. Over-all efficiency of mercury-arc rectifier units at rated load, in function of the d-c operating voltage

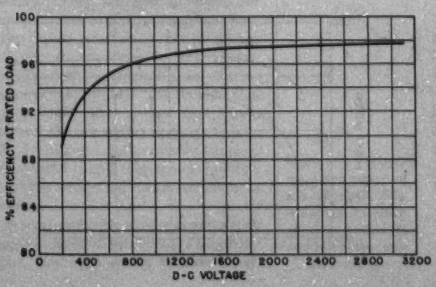
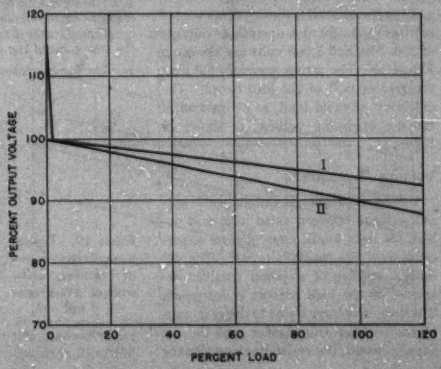


Figure 18. Typical voltage-regulation curves of rectifier units

I—For 6-phase double-wye transformer connection

II—For 6-phase star transformer connection



efficiency, voltage regulation, and power factor. These characteristics are discussed in the following, and typical values are shown by curves. The use of phase control is also considered.

Efficiency

The over-all efficiency of a rectifier unit is the ratio of the power output to the total power input, or the ratio of the power output to the power output plus the losses. The efficiency is determined usually by calculation from the measured segregated losses in the component parts of the unit. The losses of a typical unit consist of the core and load losses of the rectifier transformer, the arc-drop losses in the rectifier tubes, and the power required for tube excitation and the auxiliaries.[27]

The rectifier transformer loss is usually in the range of 1.5 to 2.5 per cent of the kilowatt output, depending on the kilovolt-ampere size and the voltage. The arc drop in the rectifier tubes ranges from 15 to 25 volts, depending on the size and type. The arc drop of a tube changes little with the load current, and the same tube can be used for a wide range of d-c operating voltages. The per cent loss in the rectifier therefore decreases as the d-c voltage is increased.

The excitation losses are of the order of 200 to 300 watts per tube. The auxiliary losses include the power required for such items as the water pump, vacuum pumps, and vacuum gauge, and usually constitute a fraction of one per cent of the rectifier output. Sealed-tube rectifiers have no evacuating equipment, and may not require a water pump when cooled by tap water. When water heaters or anode heaters are used, they generally operate only intermittently, and their losses are not considered in the efficiency calculation.

Typical over-all efficiency curves of rectifier units for d-c operating voltages of 250, 600, and 3,000 volts are shown in Figure 16. The curves are fairly flat over the greater part of the load range. The efficiency at rated load, as a function of the d-c operating voltage, is shown in Figure 17.

Voltage Regulation

Voltage regulation is the change in output voltage between rated load and no-load (or light load), expressed as a percentage of the rated voltage.[27] The d-c output voltage of a power rectifier decreases as the load current is increased. The drop in voltage is due to the reactance of the rectifier transformer and the a-c supply circuit, the resistance losses in the rectifier transformer, and the increase in arc drop with current.

The voltage drop due to reactance is the major component of the regulation. It is caused by reduction of the terminal voltages of the transformer d-c winding during the commutation periods between phases (see Appendix). The reactance drop depends on the type of transformer connection used and is directly proportional to the load current.[1]. The voltage drop due to the transformer resistance is also directly proportional to the current. The increase in arc-drop with current is only a few volts between light load and rated load and is usually proportional to the current. The regulation curve of a rectifier nuit is therefore a straight line.

Typical regulation curves of rectifier units, for two types of rectifier transformer connections, are shown in Figure 18. Curve *I* applies to a 6-phase double-Y connection (see Figure 14), which has been the circuit most generally used for mercury-arc rectifiers. The voltage rises about 15 per cent between light load (0.5 to 1.0 per cent load) and no load, because there is insufficient current for excitation of the interphase transformer. This voltage rise can be suppressed by connecting a ballast resistance when the load current drops to zero. For most applications this is not required. Curve *II* shows the regulation for a 6-phase star connection. Although this connection is used rarely, the curve is given to illustrate the effect of the rectifier circuit on the regulation.

Power Factor

The power factor of a rectifier unit is the ratio of the volt-amperes to watts at the a-c line terminals of the rectifier transformer. The power factor is less than unity for two reasons.

1. The a-c line current is nonsinusoidal and contains harmonic components which do not contribute to the power input but increase the rms value of the current.
2. The fundamental component of the line current is displaced in the lagging direction from the line-to-neutral voltage, because of the transformer and line reactances.[27]

As the number of rectifier phases is increased, the harmonic components of the current decrease and the power factor increases, approaching the power factor of the fundamental component. Typical power factor curves of 6-phase and 12-phase rectifier units are shown in Figure 19.

Voltage Control

It is often desired to control the ouput voltage of a rectifier for various reasons, such as to maintain a constant voltage on the d-c bus or a constant output current, for starting a motor at reduced voltage, or for controlling the motor speed. The output voltage can be changed by means of taps on the rectifier transformer. However, this method is not practical for continuous control of the voltage, unless load tap-changing equipment is employed.

For most rectifier applications, phase control is used for controlling the output voltage. The voltage is reduced below its normal value by retarding the firing points of the anodes in their voltage cycles by means of grid or ignition control (see Appendix). This method is illustrated by the inclined parallel lines in Figure 20, which are voltage-regulation lines for several values of the phase-control angle of retard. The angle can be changed continually and smoothly by means of a small rheostat or regulator. The horizontal line in Figure 20 illustrates constant-voltage control, for maintaining rated voltage from no load to full load. The difference between this line and the top regulation line represents the amount of phase control required; the angle of retard has to be varied between 0 and 20 degrees.

When phase control is used, the a-c line current is shifted in the lagging direction, which lowers the power factor by approximately the same ratio as the reduction of

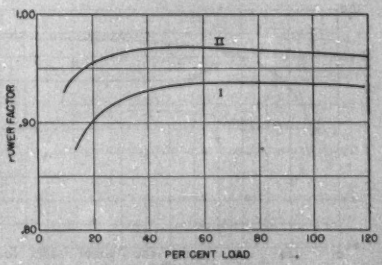


Figure 19. Typical power-factor curves of rectifier units, without phase control

I—For 6-phase unit
II—For 12-phase unit

hot output voltage. This is shown by the power-factor curves in Figure 21. Curve *I* is for operation without phase control, curve *II* for 10 per cent reduction of voltage by phase control, and curve *III* applies to constant-voltage control as represented by the horizontal line in Figure 20.

Phase control also increases the duty on the rectifier tubes and the probability of arc-back. Only the amount of phase control actually needed should be used. When phase control is required, a range of about 15 per cent is usually provided in the control, but the full range is used infrequently.

Part V. Application of Rectifiers

General

Since their introduction in 1924, metal-tank mercury-arc rectifiers have become the accepted alternating- to direct-current power converters in North America, for most applications requiring d-c power of about 200 kw or more, at d-c operating voltages of about 250 volts and higher. The increase in the fields of application was a gradual process and was the result of improvements in the performance of rectifiers, development of new types, and pioneering by individuals and companies in new applications. This process is continuing.

The characteristics of rectifiers largely responsible for their preference over rotating-type converters were given in Part I. They are stationary and quiet, are simple to install and operate, have a high-efficiency and a low-maintenance cost. There are other specific advantages in the operation of rectifiers. There is no problem in parallel operation of rectifiers, because their polarities are fixed, reverse current cannot flow normally, and their voltages can be controlled easily and quickly. They are also adaptable for parallel operation with rotating machines. They can be returned to service quickly following an outage or power interruption, an important consideration in electrochemical plants and many industrial applications. They cannot feed into a fault on the a-c system and usually can ride through such faults without tripping. Being water cooled they can be installed in places where ventilation would be a problem.

Several problems were encountered in the application of rectifiers. In the earlier years they were cooled by raw water, which caused corrosion on the vacuum tank and water jacket. This was overcome by using a recirculating cooling system with a heat exchanger, which has become standard practice for pumped rectifiers. When rectifier installations were made on high-capacity a-c and d-c power systems, some failures occurred on rectifier transformers, due to high fault currents during arc-backs. This was solved by using high-speed circuit breakers and by improvements in transformer design. Such failures rarely occur now.

The harmonics in the d-c output voltage and the a-c line currents sometimes cause interference in telephone lines exposed to the a-c or d-c circuits. Interference from the d-c circuit, which has occurred only on railway systems, was eliminated by filtering the output voltage.[8] Telephone interference from the a-c systems supplying large rectifier installations in electrochemical plants was a serious problem. It was solved by multiplying the number of rectifier phases, for a group of units operating in parallel, by shifting the phase voltages of individual units.[12] There have been few cases of telephone interference from a-c systems supplying small rectifier installations. Such interference can be eliminated sometimes by changes in the power system or telephone circuits. Filters can be used to reduce the flow of harmonic currents into the system when other remedies are not possible.[8]

Electric railways were the first large users of d-c power to adopt rectifiers, because of their high all-day efficiency at the relatively low-load factor encountered in railway service. Quiet operation, ability to take high-load swings, and adaptability for automatic control were other considerations favoring rectifiers.

The application of rectifiers for electrochemical service came next, following the development of high-current multianode rectifiers rated at 5,000 amperes. The first installation was made in a Canadian zinc plant in 1929, with rectifiers of European manufacture. Applications for hydrogen and chlorine plants followed several years later. In 1938 the first large rectifier installations were made in aluminum reduction plants, in the United States and Canada. The production of aluminum was expanded greatly during the war, and this industry has become by far the largest user of mercury-arc rectifiers. Several large rectifier installations for production of magnesium also were made during the war. Rectifiers were adopted for electrochemical service because they are stationary equipment and have no commutator, are more stable than rotary converters under system disturbances and can be re-started quickly following a power outage, are simpler to operate, and their operating and maintenance costs are lower.

The extensive application of rectifiers for 250-volt constant-voltage industrial service, in steel mills and other industrial plants, followed the development of the single-anode type rectifiers with their improved efficiency at this voltage. The development of sealed-tube rectifiers extended the field of application to lower ratings, between 150 and 400 kw, where motor-generator sets were used formerly. Rectifiers are given preference in this service for the same reasons as for the other fields of application. They have a higher all-day efficiency than rotating machines. They can be placed in locations close to the load center and are simple to operate.

The increasing use of rectifiers in coal mines, for supplying d-c power at about 275 volts, is another consequence of the development of sealed tubes. Unit ratings of 200 to 600 kw are generally used, and many of the units are portable.

A start has been made in the use of mercury-arc rectifiers with a wide range of phase control for variable-speed motor drives. Another recent application is

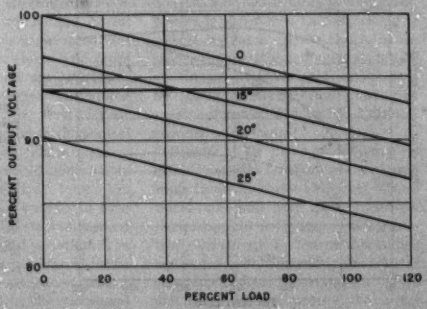


Figure 20. Voltage-regulation curves of a rectifier unit, illustrating the operation of phase control for regulating the output voltage

their use for field excitation of large high-speed turbine generators, as a substitute for direct-connected d-c exciters. High-voltage mercury-arc rectifiers have been applied for supplying plate power to radio-frequency oscillators, when several hundred kilowatts or more are required. This field has been limited so far, and its future growth depends on the use of higher power radio stations and increased power for radio-frequency electronic heating in industry.

Another recent development is the application of mercury-arc rectifiers in electronic frequency changers. One type of frequency changer is used for interconnection of a-c power systems through a nonsynchronous tie. Another type converts low-frequency power to a higher frequency for induction heating and melting of metals.

The various applications of rectifiers discussed in the preceding paragraphs are covered more fully in the following sections. Service requirements and methods of operation are described, and some typical power-circuit diagrams and illustrations of installations are shown.

The power-circuit diagram and component equipment of a typical rectifier unit were described in Part III. The a-c circuit breaker and rectifier transformer may be placed indoors or outdoors. The rectifier with its auxiliaries, the d-c and anode circuit breakers, and the control equipment are indoors. The rectifier is insulated from ground, except when the positive d-c terminal is grounded. Sealed-tube units generally are built as indoor unit-type substations. The equipment is mounted and wired in metal-enclosed cubicles, which are lined up in sequence according to the power circuit. The rectifier transformer is between the a-c circuit breaker cubicle and the rectifier cubicle, which is followed by the d-c circuit breaker cubicle.

Table I is a summary of the estimated kilowatt capacity of rectifiers for various applications, installed or on order in the United States and Canada, up to January 1, 1948. Rectifiers that have been transferred from their original application to a new application are included under the new application.

In addition to the capacities in Table I, rectifiers with a total capacity of about 618,000 kw were manufactured in the United States for export to countries outside of North America. About two-thirds of that capacity was for production of aluminum, about one-fourth for transportation service, and the balance for other applications.

Railway Service

Voltages and Ratings

Rectifier unit sizes in the range of 500 to 3,000 kw are used for railway service; most of them, however, are 1,000 kw or higher. Three classes of d-c voltages are used for electric railways—600, 1,500, and 3,000 volts. By far the largest number of rectifier installations are for 600-volt service. This voltage is used for street railways, subways, suburban, and interurban lines. It is used also for some electrified sections of railroads. The higher voltages are used for suburban and interurban service and for main-line electrification.

There are three classes of ratings for conversion equipment used on railways.[27] All three have a rating of 150 per cent of rated current for two hours, following continuous operation at rated load. This overload rating is intended to take care of the morning and evening rush-hour periods on urban and suburban lines. The three classes of ratings differ in regard to their short-time overloads, following operation at rated load. These overloads are intended to take care of the load swings during starting and acceleration.

(A) Class I, for light-duty service, as on street railways, has a one-minute rating of 200 per cent of the continuous current rating.

(B) Class II, for medium-duty service, encountered on rapid-transit systems such as subways, has a one-minute rating of 300 per cent of rated current.

(C) Class III, for heavy-duty service, as for main-line railroad electrification, has a 5-minute rating of 300 per cent of rated current, to take care of the longer acceleration time required for heavier trains.

Substations[27]

On street railway systems the substations generally are distributed over the system, with one or two units per station. This is done to reduce the length and size of d-c feeders and to maintain a stable voltage on the trolley wire. On rapid-transit lines and railroads the substations usually are located along the line, at spacings depending on the voltage and load. The quiet operation of rectifiers and the nonrequirement of special ventilation have made it possible to place the substations in residential areas, or other locations, selected on the basis of economic and engineering considerations.

The rectifier substations usually are unattended and are controlled automatically or by supervisory control, with automatic reclosing in case of a trip-out due to a fault.[7] When full-automatic control is used, the rectifier units are started and stopped by a time switch or by load demand. Load-demand control is used in substations which have long idling periods. The rectifier is started when the d-c line voltage drops below a predetermined value for several minutes, indicating that a load is approaching; it is stopped when the load drops and remains low for 10 to 15 minutes. In substations having two or more units, additional units are started, in a predetermined sequence, when required by increased station load, and are shut down when the load drops off.

The a-c circuit breakers and rectifier transformers are placed outdoors when that is permissible. In densely populated areas all the equipment is usually placed indoors. When space is limited or ventilation is inadequate for self-cooled rectifier transformers, water-cooled transformers are used. On most railway systems the negative side of the d-c circuit is connected to the running rails and is therefore grounded. The positive ter-

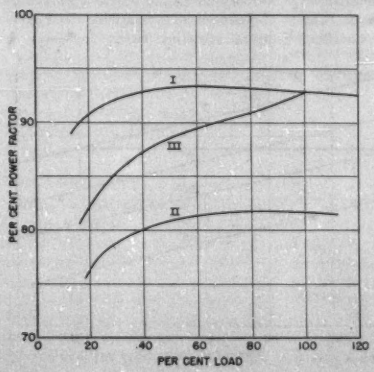


Figure 21. Power factor curves of 6-phase rectifier unit with and without phase control

I—No phase control
II—Output voltage reduced 10 per cent by phase control
III—Output voltage maintained constant between no load and full load by phase control

Figure 22. Rectifier installation for 600-volt urban railway service

Unit rated at 3,000 kw. Rectifier transformer is water cooled

minal of the rectifier is connected to the d-c bus through a single-pole d-c circuit breaker.

Most of the rectifiers in railway service were installed before the single-anode rectifiers were developed and are of the multianode type. Pumped single-anode rectifiers were used in practically all the installations made since 1940. Recently, several sealed-tube units rated at 1,000 kw were put in operation. In Figure 22 is shown a view of a rectifier installation in railway service.

New York Subway Rectifiers

The largest installation of rectifiers for railway service in the world was made on the city-owned subway system in New York City, between 1930 and 1940. The aggregate capacity of rectifiers on that system is about 330,000 kw. The rectifier units are rated at 3,000 kw, 625 volts, with 150 per cent of rated load current for two hours, 200 per cent for five minutes, and 300 per cent for one minute.

An interesting feature of the subway installations is that underground substations are used, located adjacent to the subway tracks. This effected considerable savings by obviating the purchase of substation sites in congested areas, and was possible because the ventilation problem was eliminated by using rectifiers. The rectifier transformers are also water cooled. A perspective drawing of an underground substation is shown in Figure 23. During installation, the equipment was lowered into the stations through openings from the street, which were then sealed off. There are also a few surface substations, with several rectifier units per station. The substations are automatically operated, with supervisory control and telemetering at a central dispatcher's office.

Delaware, Lackawanna and Western Electrification

Another interesting installation of rectifiers was made in 1930, for the 3,000-volt electrification of the suburban section of the Delaware, Lackawanna and Western Railroad, between Hoboken and Dover, N. J.[2] The 3,000-volt d-c power is supplied from five rectifier substations, with a total capacity of 39,000 kw. Two substations have four 3,000-kw units each, two substations have two 3,000-kw units each, and one substation has two 1,500-kw units. All rectifier units have a 12-phase connection and a Class III heavy-duty railway rating. The four larger substations are manually operated. The fifth substation is automatic, with supervisory control from the nearest attended substation. The substations are spaced from 7 to 15 miles apart.

Electrochemical Service[43]

Operating Conditions

In electrochemical service, the load circuit consists of many electrolytic cells connected in series. The same current flows through all the cells, and the voltage per cell is a characteristic of the electrolytic process. There is no solid ground on the circuit; however, there usually is a floating ground due to leakage current between cells which are at different potentials in the circuit. In some installations the mid-point of the circuit is grounded through a resistor.

Electrolytic cells are characterized by a counter-electromotive force and resistance. The counter-electromotive force voltage is between about 30 and 75 per cent of the applied voltage, at rated current, for the different electrolytic processes.[22] When the power supply is interrupted, the counter-electromotive force decreases gradually to zero. Some electrolytic circuits with a high counter-electromotive force will take a high inrush current when power is applied after the counter-electromotive force has dropped to a low value or zero. In some cases it is necessary to start at reduced voltage or with a resistor in series. The cell circuits also have inductance due to the loop traversed by the load current.

Rectifiers are used in electrochemical service for d-c operating voltages from 250 to about 800 volts. In most applications, however, the operating voltage is 500 volts or higher. The conversion equipment operates continually at about full load. In addition to the continuous rating, the equipment has a 150 per cent rating for one minute. The voltage regulation (that is, the drop in output voltage between no load and full load) of a rectifier unit is of no importance in electrochemical service, because the load current is constant. The rectifier transformers are designed usually with a high reactance, of the order of 8 to 10 per cent, in order to limit the magnitude of fault current during an arc-back.

The rectifier output voltage can be adjusted by phase control, to compensate for variations of the a-c supply voltage or for changes in the cell-line. A phase-control range of about 5 per cent is usually sufficient, although a larger range is used sometimes for short periods. In applications requiring a large range of voltage adjustment for continuous operation, the a-c voltage of the rectifier transformers is varied by a load tap-changing voltage regulator. If the change in the level of the operating voltage is made only at infrequent intervals, this can be done by taps on the rectifier transformer or an autotransformer, with a tap changer operated at no load.

Because of the high current ratings required, pumped rectifiers are used generally for electrochemical service. (Some sealed-tube units have been applied for electrochemical processes requiring relatively low currents). The rectifier station layout does not differ greatly from that of other applications. The equipment of each unit is arranged in sequence of the current

Table I. Estimated Capacity of Mercury-Arc Rectifiers Installed or on Order up to January 1, 1948

Application	United States, Kilowatts	Canada, Kilowatts
Electrochemical		
Aluminum	1,743,000	1,201,000
Chlorine	680,000	11,000
Magnesium	122,000	
Zinc	29,000	29,000
Hydrogen		61,000
Others	98,000	
Total	2,672,000	1,302,000
Railway		
600 volts	714,000	57,500
1,500 volts	42,000	3,000
3,000 volts	46,000	1,500
		(2,400 volts)
Total	802,000	62,000
Industrial	247,000	11,000
Mining	149,000	
Sub-totals	3,870,000	1,375,000
Grand total	5,245,000 kilowatts	

flow between the a-c and d-c busses. A basement, or low-headroom ground floor, is usually provided below the rectifier room, for the electrical interconnections between the component parts of the units, for the water piping, and the control wiring. Sometimes the cooling equipment and circuit breakers are also placed there.

The control cubicle, which contains the excitation and control equipment for the individual rectifiers, is placed next to the rectifier. The station is operated from a central control switchboard, on which are mounted the indicating instruments and the control switches for all the circuit breakers. A section of a typical rectifier station in an electrochemical plant is shown in Figure 24. The connection between the rectifier transformer and the negative bus is placed near the ceiling of the rectifier room, to increase the spacing between the positive and negative connections. This provides a higher inductance in the d-c circuit, which reduces the harmonic components in the direct current of the individual units.

Aluminum

The total installed capacity of rectifiers for aluminum production, in the United States and Canada, is about 3,000,000 kilowatts, which is nearly 60 per cent of all rectifier applications. The aluminum plant at Arvida, Canada, has the largest rectifier installation in the world in one location, with a total capacity of 747,000 kw. A typical aluminum cell-line or potline is supplied with a direct current of 55,000 to 60,000 amperes at about 650 volts. Most of the Canadian installations operate at 700 volts. The counter-electromotive force of the potline is about 30 per cent of the operating voltage. Approximately 10 kilowatt-hours of energy is required per pound of aluminum produced. The a-c power for the rectifier stations is obtained usually from hydroelectric power stations over high-voltage transmission lines. The voltage is stepped down to about 13 kv by transformer banks near the rectifier station.

A single-line power-circuit diagram of a typical rectifier installation for two aluminum potlines is shown in Figure 25.[15,19] Six 10,000-ampere rectifier units supply each potline. Each unit consists of two rectifiers connected to a rectifier transformer with two d-c windings, which have a 6-phase double-Y connection as shown in Figure 14. Each rectifier usually consists of 12 single-anode pumped tubes and is connected to its 6-phase transformer d-c winding through six anode-paralleling reactors and a 6-pole

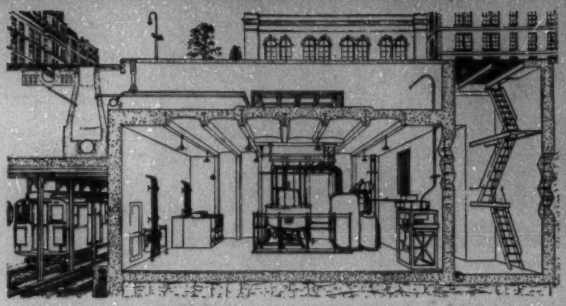


Figure 23. Underground rectifier substation on New York City subway system

anode circuit breaker. The cathode terminal of each rectifier is connected to the positive d-c bus through a semi-high-speed d-c circuit breaker. The negative terminals are connected to the negative d-c bus through disconnecting switches.

Three of the rectifier transformers have delta-connected a-c windings, and three have Y-connected windings. By this combination and the 10-degree phase-shifting autotransformers on four units, a 36-phase rectifier system is obtained for the six units. This eliminates from the a-c line current the harmonic components which could cause interference in communication circuits.[1]

The rectifier units can be connected to either 3-phase a-c bus by means of air-break disconnects, which are capable of interrupting the magnetizing current of the rectifier transformers. Two a-c busses are used, to provide greater flexibility in supplying power for the two potlines and to insure continuity of service in case of accidental damage on one bus. Power to the a-c busses is supplied through a-c circuit breakers.

An arc-back in an individual rectifier is cleared by its anode circuit breaker, overload protection is provided by the d-c circuit breaker. The rectifier transformers and phase shifters are protected by overload relays, which are connected to current transformers on the individual units and trip the a-c line circuit breakers in case of a fault in a transformer or between a transformer and the anode circuit breakers. The relays do not operate during an arc-back.

There is no circuit breaker between the d-c bus and the potline circuit. To apply power to the potline following an outage, grid or excitation control of the rectifiers is utilized to make all the rectifiers of one potline start conducting simultaneously, after the circuit breakers have been closed. Power to a potline is interrupted by simultaneous tripping of the d-c circuit breakers of all the units supplying the potline. A tie circuit breaker is provided between the positive d-c busses of the two potlines. This tie is closed normally and aids in equalizing the load among the rectifier units, in case a rectifier should trip out or is taken out of service for maintenance. The tie circuit breaker is opened before power on a potline is switched off.

Auxiliary power transformers are installed to supply power for the excitation, vacuum pumps and cooling equipment of the rectifiers, and for other power requirements in the rectifier station. Each rectifier is cooled by a recirculating water system, with a water-to-water or a water-to-air heat exchanger. In many installations in the United States, water-to-air heat exchangers are used and are located on the roof of the rectifier building.

Another typical power-circuit diagram of a rectifier installation for an aluminum or magnesium plant is shown in Figure 26.[20] The connection of the rectifiers is the same as in Figure 25. However, in Figure 26 metal-clad, draw-out type a-c switchgear is used on the 13.8 kv circuit. A circuit breaker is used between each rectifier unit and the a-c bus, in addition to the anode circuit breakers. The 13.8 kv supply system is sectionalized, to limit the kilovolt-ampere interrupting capacity required for the circuit breakers. The auto-transformers shown in Figure 26 are required in some installations for reducing the d-c output voltage to a low value when starting a new cell-line.

Anode circuit breakers were not employed in the rectifier installations of the Canadian aluminum plants. An a-c circuit breaker was used between the a-c bus and each rectifier unit, and a high-speed d-c circuit breaker with reverse-current trip was used between each

rectifier and the positive d-c bus. This arrangement was employed also in the United States for the first rectifier installation in an aluminum plant.[18]

All the operating functions for the electrical equipment of two to four aluminum potlines are performed from one central switchboard. On this switchboard are mounted the indicating instruments, the control switches for the a-c line and rectifier circuit breakers, the voltage-control switches of the rectifiers, the control switches for the potlines, and other station controls.

A relatively small range of phase control is used for adjusting the d-c output voltage, usually less than 5 per cent. In some installations, where large variations in the a-c supply voltage occur, automatic load tap-changing voltage regulators are used for regulating the a-c bus voltage.

In Figure 27 is shown a view of a rectifier installation with 24 5,000-ampere rectifiers for two aluminum potlines. The anode circuit breakers are near the left wall. The rectifier control cubicles are at the right of the rectifiers, and the d-c circuit breakers are near the right wall. The d-c busses were located outdoors, outside of the right wall of the building. The water-to-air heat exchangers were on the roof of the building. Figure 28 shows a view of the a-c bus structure, rectifier transformers, and phase shifters in a rectifier installation for production of aluminum.

Magnesium[45]

Several large rectifier installations for production of magnesium were made during the war. Many of these rectifiers have since been transferred to other applications, mostly for production of chlorine and allied processes.

In so far as the power conversion equipment is concerned, a magnesium cell-line is similar to an aluminum potline, and the power-circuit diagram of Figure 26 is typical for this application also. A large magnesium cell-line operates normally with a direct current of 60,000 amperes at 600 volts. In one large magnesium plant built during the war, 20,000-ampere 350-volt cell-lines were used. A rectifier installation for a 60,000-ampere 600-volt magnesium cell-line is shown in Figure 29. The anode circuit breakers are at the left, and the control cubicles and d-c circuit breakers are at the right of the rectifiers.

The counter-electromotive force voltage of magnesium cells is approximately 35 per cent of the applied voltage. A wider adjustment of the voltage is required for a magnesium cell-line, because a new cell-line is started with only a few cells in series, and additional cells are added to the circuit over a period of several weeks. An autotransformer with load ratio control is used ahead of the a-c bus to provide the needed voltage range. Normally the cell-line operates at approximately full voltage.

Chlorine[20-43]

A typical chlorine cell-line operates with 7,500 to 10,000 amperes d-c at 500 to 700 volts, depending on the type of cell and the number of cells connected in series. However, there is a trend towards the use of higher-current cells. The counter-electromotive force of a chlorine cell is approximately 65 per cent of the applied voltage.

Usually each cell-line is supplied by one or two rectifier units, without paralleling with other cell-lines on the d-c side except in an emergency. Some cell-lines are designed for 1,800 to 2,000 amperes, and a number of these may be operated in parallel. A relatively small range of voltage control is required in normal operation, and is effected by phase control on the rectifiers. When starting a cell-line which has no counter-electromotive force, the voltage has to be reduced to about 30 or 40 per cent of normal for a short time, to limit the initial inrush current until the counter-electromotive force is established. Sometimes a resistor is connected in series with the cell-line on starting, to limit the inrush current.

A typical rectifier unit for a chlorine cell-line consists of two rectifiers connected to one transformer, similar to one of the units used in aluminum plants. The rectifier transformer is connected to the a-c circuit through an a-c circuit breaker. High-speed or semi-high-speed d-c circuit breakers are used between the rectifiers and the d-c bus. In the more recent installations, high-speed anode circuit breakers and semi-high-speed d-c circuit breakers are employed. Sometimes a single d-c circuit breaker is used for both rectifiers.

Zinc[30]

A typical zinc cell-line operates with 10,000 amperes direct-current at 500 or 600 volts. Each cell-line is supplied by one rectifier unit consisting of two rectifiers and one transformer, with an a-c circuit breaker and a high-speed d-c or anode circuit breaker. The counter-electromotive force of a zinc cell-line is about 75 per cent of the applied voltage.

A zinc cell has to be taken out of service periodically for stripping the zinc from the electrodes. To facilitate disconnecting the cell, the current of the circuit is reduced to a low value for a short time by reducing the voltage by about 25 per cent. A separate regulating transformer with load-tap-changing, or taps on the rectifier transformer, are used for reducing the voltage. When a cell-line is started with low or zero counter-electromotive force, the voltage is reduced to a low value by the regulating transformer and phase control on the rectifiers.

Because of the high counter-electromotive force of zinc cells, the ripple in the output voltage of a 6-phase rectifier will produce an appreciable ripple current. A reactor is connected in series with the rectifier to limit this current. A reactor is not required for a 12-phase rectifier.

Hydrogen[4]

A typical hydrogen cell-line operates with 10,000 amperes at 600 to 800 volts. Each cell-line is supplied by a separate rectifier unit, the same as for zinc cell-lines.

A water rheostat is connected in series with the cell-line on starting, to limit

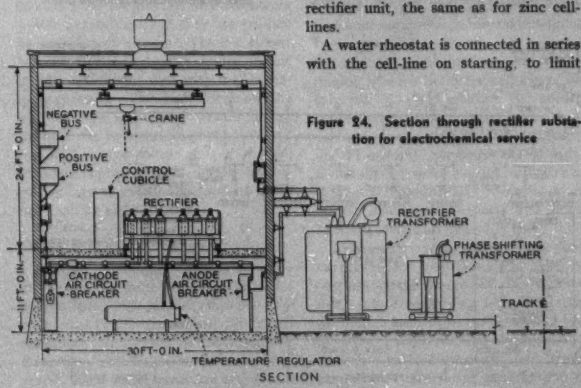


Figure 24. Section through rectifier substation for electrochemical service

inrush current. The same rheostat is used when the cells are short-circuited for depolarizing.

Industrial Service

Applications

Mercury-arc rectifiers are used for supplying d-c power, at substantially constant voltage, in many types of industrial plants such as steel mills, foundries, smelters, paper mills, rubber plants, printing plants, metal fabricating plants, and others. The power is used for operating cranes, conveyors, electromagnets, ore and coal handling equipment, machine tools, and other motor-driven equipment using d-c motors.

Unit sizes between about 150 and 1500 kw are used, at various d-c output voltages between 125 and 600 volts. However, for most applications the operating voltages are between 240 and 275 volts. Rectifier units for industrial service have a rating of 125 per cent of rated load for two hours and 200 per cent for one minute, in addition to the continuous rating. The rectifier transformers are designed for a temperature rise of 60 degrees centigrade at the 2-hour rating.

When a 250-volt rectifier is applied on a 250/125 volt 3-wire d-c circuit, a balancer set is generally used to provide the middle wire. It consists of two 125-volt rotating d-c machines coupled mechanically and connected in series. The balancer set is connected in parallel with the rectifier, and the middle wire of the d-c circuit is connected to the junction point between the two machines of the balancer. The current of the balancer is one-half the current of the middle wire. Instead of using a balancer, a 3-wire circuit can be obtained with rectifiers. However, such a circuit consists essentially of two 125-volt rectifiers in series, which would have a poor efficiency.[28]

While most of the rectifiers in industrial service are for constant output voltage, some rectifiers with a wide range of voltage control have been installed for specific motor drives. The voltage is reduced to a low value for starting the motors, but a smaller range of voltage control is needed in normal operation. So far, rectifiers have not been applied for reversing-mill drives, because the need for regeneration and the desirability of using stored flywheel energy for supplying the recurrent high load peaks make motor-generators more favorable.

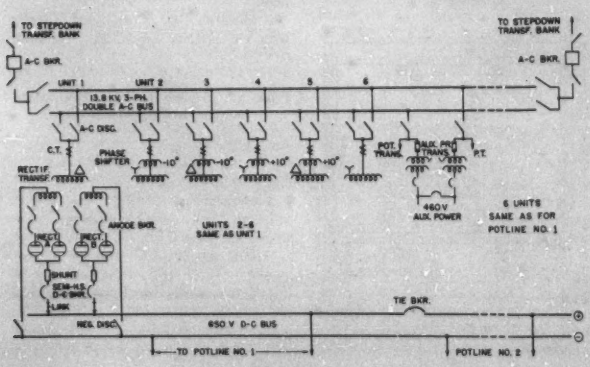
Rectifier Units

Sealed-tube rectifiers are used generally for unit ratings below 600 kw. Pumped-tube rectifiers are usually applied for higher ratings. The rectifier circuit of Figure 14 is used for most applications. Some small sealed-tube units were supplied with a 4-phase connection, obtained by using a Scott-connected transformer a-c winding. For most industrial applications, a-c and d-c circuit breakers are employed for switching and protection.

The tubes of pumped rectifiers are assembled on a frame with the evacuating equipment, as for other applications. In most plants the a-c circuit breakers and rectifier transformers are placed indoors. It is the usual practice to have the a-c and d-c circuit breakers mounted in metal-enclosed cubicles. In Figure 30 is shown a 1,500-kw pumped rectifier installed in a steel mill.

Sealed-tube rectifier units are supplied as factory-built unit-type substations, with the rectifier equipment and circuit breakers mounted in metal-enclosed cubicles. In Figure 31 is shown a 500-kw unit with 12 sealed tubes. The sequence of the equipment from left to right is: a-c switchgear cubicles, rectifier transformer, tube cubicles, control and d-c circuit breaker cubicles, and d-c feeder circuit breaker cubicles (not shown). The rectifier tubes are seen through the open door of one of the tube cubicles. In Figure 32 is shown a sealed-tube unit with six 400-ampere tubes. The rectifier excitation equipment and the cooling-water connections are contained in the tube cubicle. The voltage regulator and other controls are usually mounted in the d-c circuit breaker cubicle. The tubes are cooled by raw water or by a recirculating system with a heat exchanger.

Figure 25. Typical single-line power-circuit diagram of rectifier installation for two potlines in aluminum-production plant



Controls

Rectifier units in industrial service are started and stopped manually, but otherwise are usually unattended. They are provided with d-c voltage regulators for maintaining constant output voltage, or some other desired voltage characteristic, by phase control. Two or more rectifiers may be operated in parallel by means of current-equalizing connections on the voltage regulators, or by current compounding to give the rectifiers a slightly drooping voltage-regulation characteristic. Rectifiers can be operated in parallel with rotating machines by matching their voltage characteristics.

When a rectifier is used to supply power to cranes or elevators which require regenerative braking, the regenerated power has to be absorbed by other loads connected to the d-c circuit, because reverse current cannot flow into the rectifier. If there is not sufficient other load to absorb this power, an automatically-controlled dynamic braking resistor is provided. The resistor is connected to the d-c bus by an overvoltage relay and a contactor whenever the d-c voltage generated by the motor with the overhauling load rises above a preset value. The resistor is disconnected automatically after a preset time. If regeneration persists, the cycle is repeated.

Mining Service

Rating

During the last decade approximately 400 mercury-arc rectifier units with a total capacity of about 150,000 kw, were sold in the United States for mining service, most of them for coal mines.[13] A large majority of them are portable sealed-

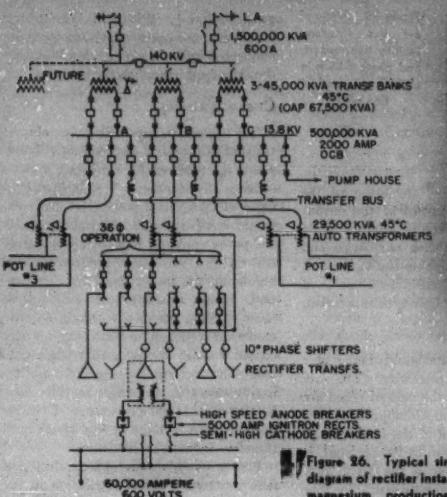


Figure 26. Typical single-line power-circuit diagram of rectifier installation for aluminum or magnesium production, using metal-clad, drawout type a-c switchgear

Figure 27. View of rectifier installation in aluminum-production plant

Twenty-four 5,000-ampere rectifiers are shown, for supplying two 650-volt potlines

tube units for ratings of 200 to 500 kw, with a d-c output voltage of 275 volts. In some installations the output voltage is 575.

Rectifier units in mine service generally have Class I railway rating, with 150 per cent of the continuous rating for two hours and 200 per cent for one minute. The load consists mostly of electric locomotives, pumps, cutters, drillers, and loading machines. Where a rectifier substation is located along the main haulage way, and the load consists largely of locomotives, Class II railway rating is used sometimes, with a 1-minute rating of 300 per cent.

Units

Stationary rectifier units are installed in surface substations, and are similar to units built for other applications. Portable underground units are mounted on low-headroom cars, with an over-all height of 42 to 52 inches above the rails. A typical portable substation is shown in Figure 33. It consists of three cars. The first car on the left contains the a-c switchgear and the auxiliary-power transformer. On the second car is mounted the rectifier transformer. The third car contains the rectifier tubes with their excitation and cooling equipments, the d-c switchgear, and the control panel. The rectifier is cooled by a recirculating cooling system with a water-to-air heat exchanger. For safety reasons, the sides of the cars are enclosed by covers so that no live parts are exposed.

This type of unit can be moved easily to new locations, close to the load center, as mine operations progress. It is placed on a siding in the mine tunnel, and the only external connections are the a-c supply leads and the outgoing d-c leads. One of the d-c terminals is grounded.[42]

Control

Most of the rectifier units in mining service are semiautomatic. The unit is started and shut down by a pushbutton, but operates automatically after it is started. The a-c and d-c circuit breakers reclose automatically if they are tripped by overload or a fault. The d-c circuit breaker serves also as a feeder circuit breaker and is provided with load-measuring controls. This control feels out the load through a bridging resistor across the open circuit breaker and closes only if the load is not excessive. A voltage regulator is provided to maintain the output voltage substantially constant up to full load, by phase control.

Operation

Rectifiers were adopted for mining service for the same reasons as in other applications: high all-day efficiency, low maintenance cost, simplicity of control and operation. A comparative efficiency test made on a rectifier, a synchronous converter, and a motor-generator set, over a period of one month, in a coal mine operating at 275 volts with a load factor of approximately 25 per cent, gave the following results: rectifier, 84 per cent efficiency; synchronous converter, 77 per cent efficiency; motor-generator, 69 per cent efficiency.

Variable-Speed Motor Drives

Application

Induction motors are used for the majority of industrial applications, because of their simple construction and the advantages of 3-phase a-c power distribution. However, d-c motors are required for many variable-speed drives. The need for adjustable-speed d-c drives is growing because of the high standard of performance required in some modern industrial operations. Where d-c power is available, d-c motors can be applied readily when a limited speed range, obtainable by field control, is needed. Where only a-c power is available, an a-c to d-c converter must be used for operating d-c motors. For motor drives with a wide range of speed control, or where regenerative braking and reversing is required, motor-generator sets with Ward-Leonard control are generally used.

About ten years ago, single-phase thyratron-tube rectifiers, with variable output voltage by phase control, were first used for supplying adjustable-speed d-c motors from an a-c system. This type of motor drive has found application in sizes of 1 to 15 horsepower and is used also where regenerative braking and re-

Figure 28. View of a-c b s structure, rectifier transformers, and phase shifters in aluminum - production plant

versing is required. The application of electronic d-c motor drives has been extended recently to motor sizes in the range of 100 to 600 horsepower, by using sealed-tube pool-cathode mercury-arc rectifiers. These applications are made preferably with 550-volt motors and where reversing is not required. More than a score of applications have been made for printing press drives, with rectifiers rated between 150 and 300 kw. The electronic drive provides for complete control including starting, jogging, low-speed running, acceleration, and emergency stopping.

Several applications have been made also with large pumped rectifiers for adjustable-speed main motor drives in steel mills, including a 3,500-kw rectifier for a continuous tube mill. The rectifier is designed for continuous operation with 40 per cent voltage reduction by phase control and with 95 per cent reduction for starting. The use of phase control lowers the power factor of the rectifier unit by about the same ratio as the voltage reduction. When a large reduction in voltage is required for continuous operation, it is preferable to obtain the reduction by taps on the rectifier transformer.

Connection Diagram

An elementary diagram of an electronic d-c motor drive with a sealed-tube rectifier is shown in Figure 34. A 6-phase double-way rectifier connection is used, which provides the required output voltage for a 550-volt motor when connected to a 3-phase 460-volt a-c supply. When the a-c supply voltage is higher, the voltage required for the rectifier is obtained by means of a step-down transformer.

By using a 6-phase rectifier, the distortion of the direct current due to the ripple in the rectifier output voltage is minimized, and a d-c motor approximating conventional design can be used.

Current-limiting reactors are connected to the a-c circuit, for protection of the tubes against excessive fault currents in case of an arc-back in the rectifier or a d-c short circuit. The auxiliary power required for the rectifier excitation and controls is supplied from the a-c circuit. The excitation circuits of rectifiers and the methods of retarding the firing of the anodes for phase-control of the output voltage were described in Part II. The rectifier tubes with their auxiliaries, and all the switching and control equipment are mounted and wired in metal-enclosed cubicles similar to a unit-type substation.

Controls

The acceleration and speed of the motor are controlled automatically. The rate of acceleration and the desired speed are set manually by means of adjustable controls. Phase-control of the rectifier output voltage, from zero to 100 per cent, is used for acceleration of the drive from standstill to the required speed. The rate of acceleration is controlled by current-limit control, which does not permit the armature current to rise above a predetermined value. This method provides smooth acceleration for various motor loads and prevents overloading the equipment and supply circuit.

After the a-c circuit breaker is closed and the control is energized, the motor can be started from rest by turning the speed-control potentiometer to the desired speed setting. During acceleration to base speed (see Figure 35), full field excitation is supplied to the motor from a separate source, and the armature current is controlled automatically by phase control of the rectifier. When base speed is reached, the rectifier output voltage is a maximum, and higher speeds are attained by weakening the motor field.

When the motor has reached the speed corresponding to the speed-dial setting, it will run continuously at that speed independent of the motor load, provided the armature current does not exceed the preset limit. When the speed dial is turned to a lower setting, the motor will be decelerated by its load until the desired lower speed is reached, which is then maintained automatically.

Because reverse current normally cannot flow into a rectifier, dynamic braking of the motor by regeneration is not possible unless the rectifier is made to operate as a power inverter and the armature or field connections are reversed, which would complicate the controls considerably. A dynamic-braking resistor is provided for rapid deceleration of the motor or emergency stopping. When the "emergency-stop" button is operated, the motor is disconnected from the supply circuit and the dynamic-braking contactor is closed.

Characteristics

The calculated performance characteristics of a typical electronic 150-horsepower motor drive, for a printing press or other applications, are given in Figure 35. Good efficiency and power factor are obtained in the running speed range, where the power required is high, by operating the rectifier without phase control and regulating the motor speed by field control. These characteristics drop off at lower speeds, where the power requirements are reduced, because the rectifier output voltage is reduced by phase control. The arc-drop voltage of the rectifying elements is the major rectifier loss; it becomes a larger percentage of the reduced output voltage at low speeds and therefore lowers the efficiency.

For some types of motor load, the use of field control for speed regulation in the running range will result in a larger motor. This has to be balanced against the lower efficiency and power factor if the whole range of speed adjustment were accomplished by rectifier phase control.

Other Rectifier Applications

Power for R-F Oscillators

High-voltage mercury-arc rectifiers have been applied for supplying plate power to radio-frequency oscillators.[6] Several such installations have been made in North America, in radio transmitting stations and for testing high-power trans-

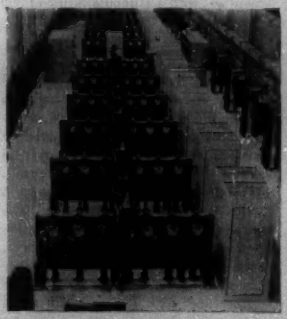


Figure 29. View of rectifier installation in magnesium-production plant

Twelve 5,000-ampere rectifiers supply a 600-volt cell-line

mitter tubes. The first installation was made in 1933, in a radio broadcasting station in Mexico. Units with ratings from 200 to 1,000 kw have been supplied for d-c output voltages between about 10 and 20 kv.

An a-c circuit breaker for connecting the rectifier unit to the a-c supply circuit is the only circuit breaker used. No d-c or anode circuit breakers are available for these voltages. The rectifier transformers of pumped high-voltage rectifiers have a d-c winding with a 6-phase star or fork connection. For sealed-tube rectifiers, the 6-phase double-way connection is preferred.[37] A series reactor and a shunt capacitor usually are connected to the d-c circuit for filtering the d-c output voltage.

An important feature of rectifiers for this application is the use of arc suppression, by means of grid or firing control, for interrupting short circuits in the load circuit and arc-backs in the rectifier, without tripping the circuit breaker. This protection interrupts the fault in one to two cycles and resets automatically to restore normal voltage in a fraction of a second. With this high-speed protection, transmitter tubes are saved from damage in case of internal flashover, and longer tube life is obtained. The quick restoration of power following a fault prevents interruption of service, an important consideration in radio broadcasting.

Mercury-arc rectifiers can be used for supplying high-power radio-frequency oscillators for induction and dielectric heating. In addition to the benefits of high-speed protection, the grid or firing control can be used for switching the power on and off, and for regulating the power output by phase control of the rectifier voltage.

Electronic Exciters

Sealed-tube mercury-arc rectifiers have been applied for field excitation of large synchronous condensers and turbine generators. The first application on a synchronous condenser was made in 1939, and several other installations have been made since then.[41] The first application on a large generator was made about two years ago.[84] A high degree of reliability is required for this service, and the rectifiers are designed so that arc-backs are cleared or tube replacements made without shutting down the generator.

A typical rectifier for this application consists of six tubes, which are connected through anode circuit breakers to a 6-phase star- or fork-connected d-c winding of a rectifier transformer. In synchronous condenser installations, the rectifier unit is supplied from the a-c system. When used for generator excitation, the rectifier unit may be supplied from the generator terminals or from the station auxiliary bus. On one application the generator was provided with a direct-connected 6-phase auxiliary a-c generator for supplying the rectifier tubes.

The equipment of the rectifier unit is mounted in metal-enclosed cubicles. Each pair of tubes, which are connected to diametrically opposite phases, with their excitation equipment and a 2-pole anode circuit breaker, are mounted in a separate cubicle compartment. To replace a tube, the corresponding anode circuit breaker is opened while the remaining four tubes continue to operate. During an arc-back the anode circuit breaker of the faulted tube trips and recloses automatically.

During a fault on the a-c system, the field excitation of the synchronous machine must be increased. To take care of this requirement and to compensate for a drop in voltage on the a-c circuit to which the rectifier is connected, the rectifier unit is designed for a considerably higher output voltage than required in normal operation. It therefore operates normally with a large amount of phase control. This provides sufficient voltage margin in normal operation even when two tubes are disconnected by an arc-back or for maintenance. The output voltage of the rectifier is regulated usually by the voltage regulator of the synchronous machine.

A relatively small number of electronic exciters of this type have been applied so far. Its advantages over d-c generator exciters are that it has a faster response, is stationary and has no commutator. Its reliability has been demonstrated in service. However, it is considerably more expensive and involves more equipment and more elaborate circuits.

Electronic Switches

Pool-cathode mercury-arc rectifier tubes are used also as switches. They are superior to mechanical contactors, because they have no moving parts, can be controlled easily and with precise timing, can handle high currents, and are completely enclosed. Their chief use as switches is for resistance welding control, with two tubes connected back to back for 2-way conductivity in the a-c supply circuit to the welding transformer. They have been applied also for discharging the stored energy of capacitors through the field coils of particle accelerators. In some of the power-converter applications, described in the preceding sections, the tubes perform switching operations in addition to their normal duty of power conversion. The difference between the two functions is that when operating as switches the flow of current in the power circuit is started or stopped, while in conversion of power the current is transferred from one part of the circuit to another in cyclic sequence.

Figure 30. Rectifier installation for 250-volt steel mill service

The unit is rated at 1,500 kw and consists of 12 pumped tubes

Part VI. Power Inverters and Frequency Changers

Power Inverter

In Part V were discussed the various applications of mercury-arc rectifiers for conversion of a-c power to d-c power. A rectifier unit used for that purpose is called a power rectifier. A rectifier unit can be used also for converting d-c power to a-c power, and when used for that purpose is called a power inverter.

Principle of Operation

The power circuit of the unit and the direction of current are the same for either operating condition. The direction of power flow between the a-c and d-c circuits is determined solely by the part of the cycle of the phase voltages during which the anode currents flow. In a power rectifier the currents flow during the positive half-cycles; the d-c voltage, which consists of portions of the phase voltages during their conducting periods, is then in the same direction as the direct current, and power flows from the a-c circuit into the d-c circuit. When operating as a power inverter, the anode currents are made to flow during the negative half-cycles of the phase voltages. The rectifier d-c voltage is then in the opposite direction from the current, and power flows from the d-c circuit into the a-c circuit (see Appendix).

Figure 31. Sealed-tube rectifier unit, rated at 500 kw, for industrial service

Twelve 200-ampere tubes are used

To make current flow through the rectifier in the normal direction, from anodes to cathodes, during the negative half-cycles of the phase voltages, the positive terminal of the d-c supply circuit is connected to the terminal of the rectifier unit at which the direct current enters. This is the reverse of the connection of a power rectifier to a d-c load circuit. The voltage of the d-c supply circuit overcomes the negative voltage of the rectifier unit and causes a flow of current. The negative voltage of the rectifier unit, when operating as a power inverter, has the same function as the back-electromotive force of a d-c motor. The analogy can be carried further if the d-c motor is assumed to drive an a-c generator for delivering power into an a-c system.

A rectifier is made to operate as a power inverter by extension of phase control into the negative half-cycles of the phase voltages. By grid or ignition control of the rectifying elements, they are kept from firing during the positive half-cycles and are made conductive at some chosen point of the negative half-cycles. This is explained with the aid of sine waves in the Appendix. For some types of transformer connections, the rectifying elements are made conductive at the latter part of the positive half-cycles, but carry over far enough into the negative half-cycles so that the average voltage is negative.

Operating Conditions

In a power inverter the current is commutated between successive phases in the same way as in a power rectifier. The rectifier transformer must be provided with alternating voltages to effect the commutation. The voltages are obtained from the a-c system, and a power inverter can operate only when connected to an a-c system which has synchronous machines, or an oscillating circuit consisting of capacitance and inductance, for generating the needed voltages.

There is one other essential condition for the operation of a power inverter. For proper commutation the center line of each phase current, or the crest of the fundamental component of the current, must come before the negative crest of the corresponding phase voltage. This means that the output current of the inverter into the a-c line leads the output voltage; that is, the inverter operates with a leading power factor. As viewed from the a-c system, the inverter requires lagging reactive kilovolt-amperes, and the a-c system must have a source of such kilovolt-amperes. This can be provided by over-excited synchronous machines. If there is insufficient synchronous capacity on the system for that purpose, a synchronous condenser or static capacitors can be used. A power inverter can operate with a power factor of approximately 85 per cent at full load, so that the reactive kilovolt-ampere is about 65 per cent of the kilowatt output.

Characteristics

The efficiency of a power inverter is the ratio of the a-c power output to the power output plus the losses. The losses in the rectifier transformer, rectifier and auxiliaries are substantially the same as for a power rectifier designed for the same a-c and d-c voltages and for operation with about 10 per cent phase control.

In considering the voltage regulation of a power inverter which is connected to a large a-c system, it can be assumed that the a-c line voltage remains constant at all inverter loads. The voltage regulation is the change in the required d-c supply voltage between no load and full load on the inverter, while operating with a fixed phase-control angle. A higher d-c supply voltage is required at full load than at no load, to compensate for the voltage drop due to the reactance and resistance of the transformer. This voltage drop increases linearly with load and has the same value as for a power rectifier operating with the same transformer. The regulation characteristic for a power inverter is therefore a straight line sloping upward with load, instead of downward as for a power rectifier.

Faults

In a power rectifier, the anode of a rectifying element is negative to the cathode during the greater part of the nonconducting period of each cycle. The major fault in a power rectifier is an arc-back, which is the loss of rectifying action during the inverse period, resulting in a reverse current to the faulted anode. In a power inverter, the anode of a rectifying element is positive to the cathode during the greater part of the nonconducting period of each cycle. The major fault in a power inverter is an arc-through, which is a loss of control causing an anode to start conducting in the forward direction during its positive nonconducting period. The result is that the back-electromotive force of the inverter disappears, and the d-c supply is short-circuited over the faulted rectifying element and its transformer phase. The ultimate fault current is limited only by the resistance of the short-circuit path and the regulation of the d-c supply circuit. If not interrupted quickly, the current will reach a high value, comparable to the reverse current during an arc-back in a power rectifier.

Figure 32. Installation of sealed-tube rectifier unit, with six 400-ampere tubes

An arc-through can be interrupted by removing the d-c power supply. Under some conditions an arc-through can recover itself, without removing the d-c supply, if the fault current has not reached a high value when the next phase of the transformer starts conducting and is able to commutate the current away from the faulted phase.

Applications

Power inverters have been applied in electronic frequency changers, which are discussed in the next section. They can be applied also for regenerative braking on d-c railroad electrification in mountainous territory, and are used for that purpose in South Africa and Europe.

Electronic Frequency Changers

Frequency changers are used to convert a-c power of one frequency to a-c power of another frequency. Two types of electronic frequency changers employing

mercury-arc rectifiers are in use. In one type, power from an a-c system of one frequency is converted to d-c by a power rectifier; the d-c power is then converted to a-c power of another frequency by means of a power inverter. The d-c circuit serves as a link between the two a-c systems (Figure 44). This type is used for interconnection of power systems. In another type of frequency changer, the power rectification and inversion take place in the same rectifying elements, and there is no distinct d-c power link (Figure 39). This type is used for producing high-frequency power for induction heating of metals. The application and operation of both types will be described.

The power transmitted by an electronic frequency changer with a d-c link can be controlled by phase control of the power rectifier or the power inverter, or both. Advancing the firing of the anodes of the power rectifier increases its d-c output voltage and the transmitted power. Advancing the anode firing of the power inverter reduces its back-electromotive force and consequently increases the transmitted power. No power will flow if the output voltage of the power rectifier is lower than the back-electromotive force of the inverter.

The direction of power flow can be reversed by shifting the phase position of the firing-controls (grid or ignition control) on the two rectifiers, from one part of the cycle of the phase voltages to another part.

No change is necessary in the power circuit connections. The firing-control circuit of each rectifier unit is supplied from the a-c system to which it is connected.

Interconnection of Power Systems

Carnegie-Illinois Installations

In 1943, the Carnegie-Illinois Steel Corporation put into operation two electronic frequency changer installations for interconnecting the 25-cycle and 60-cycle power systems in steel mills. A 20,000-kw frequency changer was installed in the Edgar Thompson plant, near Pittsburgh, Pa. A 6,667-kw unit was installed in the Gary, Ind., plant. The latter installation will be enlarged soon to 16,000 kw.

In both plants, all the power of the 25-cycle system is generated by company-owned generating equipment. The 60-cycle power system is connected to a large public-utility system. The frequency changers are utilized to transmit power between the two systems, in either direction, depending on power requirements and availability on each.

Electronic frequency changers were chosen for this service, because the 25-cycle and 60-cycle power systems do not have a fixed frequency ratio, and it was desired to regulate the transmitted power independently of voltage or frequency variations on either system. For these reasons synchronous-synchronous frequency changers could not be used. Other types of rotating machines were considered, including the Scherbius induction-synchronous converter, two rotary converters, and two a-c/d-c motor-generator sets.

The electronic frequency changer was decided upon, in spite of its higher first cost and the pioneering involved, for the following reasons: higher efficiency at all loads; ease of power flow control; ease of starting, with no starting shock on the systems and without synchronizing; absence of large rotating parts, slip rings, commutators and brushes; lower installation cost; anticipated lower maintenance cost.

The good performance of these installations has justified the decision, and credit is due to those who undertook the pioneering risk. There was sufficient synchronous capacity on both systems in these installations to supply the reactive kilovolt-ampere requirements of the power inverters.

Before describing the electronic frequency changers of the two Carnegie installations, the general application and limitations of this type of equipment will be discussed briefly.

Application[54,55]

In general, a mercury-arc electronic frequency changer can be applied economically for interconnecting two power systems, under the following conditions:

1. Where a rigid frequency relation between the systems does not exist and cannot be established by a synchronous-synchronous frequency changer of the size considered.
2. The power receiving system has a synchronous generator or other synchronous machine, such as a synchronous condenser, and enough surplus capacity of reactive kilovolt-amperes for the inverter.

Where these conditions exist, as in the steel plants mentioned, the electronic frequency changer is not only more economical than a rotating machine but is also simpler to operate and has greater flexibility. However, the opportunities for such applications are few. New loads and generating equipment in steel mills usually are for 60 cycles.

For single-phase railroad electrification, where 25-cycle power is used, much of the power is supplied by 60/25 cycle frequency changers. However, they usually are installed in locations where condition 2 does not exist. An electronic frequency changer would require a 25-cycle synchronous condenser, which would probably make it uneconomical and would cancel other advantages realized in the Carnegie installations. In cities where 25-cycle systems are still in use, synchronous-synchronous frequency changers are generally employed for interconnecting the 60- and 25-cycle systems.

Electronic frequency changers would provide an excellent nonsynchronous tie between 60-cycle systems which cannot be operated in synchronism. If the interconnection points should be located far apart, the d-c link of the frequency changer could be stretched into a d-c transmission line, operating at a suitable voltage depending on the distance. Such an interconnection would be determined mainly by economic considerations.

Gary Frequency Changer[55]

Power Systems

The 6,667-kw electronic frequency changer was installed in the Gary, Ind.,

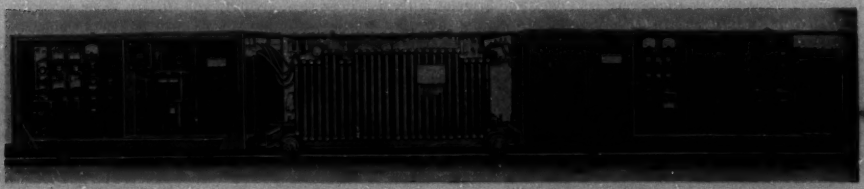


Figure 33. Portable rectifier substation for underground mine service

plant of the Carnegie-Illinois Steel Corporation, early in 1943, for interconnecting the 60-cycle and the 25-cycle power systems. All the power on the 25-cycle system is generated in United States Steel Corporation plants in the Chicago area, and a 66-kv transmission line connects the Gary and South Chicago plants. The 60-cycle system is furnished with power from the Northern Indiana Public Service Company, over 132-kv lines, and the voltage is stepped down to 6.9 kv for distribution in the plant. Each system has over 200,000 kw generating capacity. The frequency variation on the 25-cycle system is ±1.5 cycles, while the frequency on the 60-cycle system is maintained within ±0.1 cycle. This ruled out a synchronous-synchronous frequency changer.

The frequency changer is used for transfer of power between the two systems, in either direction, depending on power requirements. However, the installation was made primarily for supplying power from the 25-cycle system to the 60-cycle system, in order to control the demand on the 60-cycle power system and to increase the ratio of average power to maximum demand.

The original installation was furnished with pumped-type multianode mercury-arc rectifiers. These will be replaced in the near future with pumped single-anode excitron rectifiers, similar to the type shown in Figure 10, to increase the continuous rating of the unit to 8,000 kw with a 2-hour rating of 10,000 kw. A second 8,000-kw unit is on order, to double the capacity of the installation.

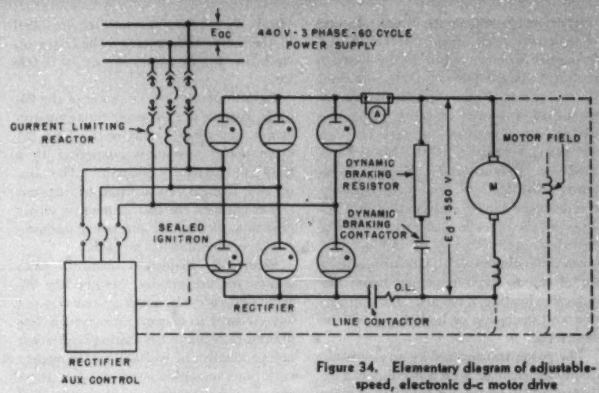


Figure 34. Elementary diagram of adjustable-speed, electronic d-c motor drive

Connection Diagram

A single-line power circuit diagram of the Gary frequency changer is shown in Figure 36. It consists of a 25-cycle and a 60-cycle rectifier units, which are connected to the respective a-c systems through oil circuit breakers. The d-c circuits of the two units are connected in series, so that the direct current flows through the rectifiers in the normal direction, from anode to cathode.

Each rectifier unit consists of a rectifier transformer and two 12-anode rectifiers, which are provided with two control grids per anode. The 25-cycle transformer is connected in delta/6-phase double-Y. The 60-cycle transformer is connected in delta/12-phase quadruple zig-zag. The rectifier transformers of the second 8,000-kw frequency changer will have their voltages shifted in phase in relation to those of the existing unit, so as to produce for the entire installation a 24-phase line current on the 60-cycle side and a 12-phase line current on the 25-cycle side. A larger number of phases is used on the 60-cycle side, because it is connected to a large public utility system and there is a greater likelihood of inductive influence on communication circuits.

The frequency changer consists of two sections, *A* and *B*, each of which can operate independently of the other. One section can be shut down for maintenance work while the other remains in operation. Such an arrangement is particularly desirable in this installation, because the frequency changer is used for controlling the maximum power demand averaged over 30-minute periods. Individual rectifiers can be isolated for maintenance work by means of disconnects.

Each section consists of a d-c winding of each transformer and the rectifiers connected to them. Considering section *A*, for example, when power is transmitted from the 25-cycle system to the 60-cycle system, 25-cycle power is converted to direct-current at 1,750 volts by means of rectifier *25-A* operating as a power rectifier, and the d-c power is converted to a-c power into the 60-cycle system by rectifier *60-A* operating as a power inverter. For power flow in the opposite direction, rectifier *60-A* operates as a power rectifier and *25-A* as a power inverter. The power circuit of the frequency changer and the direction of current in the d-c circuit remain unchanged for either direction of power flow. The direction of power flow is established by grid control of the rectifiers, which determines the position of the conducting period of each anode in the cycle of the a-c voltage applied to it (see Appendix).

The power transmitted by the frequency changer is controlled by means of a current regulator for each section, which regulates the magnitude of the current in the d-c circuit. The regulation is effected by phase control of the power rectifier, through grid control. The direct current of each section is maintained at a value corresponding to the setting of the regulator, regardless of voltage variations on the a-c power systems. The setting of the regulators can be adjusted either manually, or automatically in response to power demand.

Each section of the frequency changer is provided with a reactor and a circuit breaker in the d-c circuit. One purpose of the reactor is to reduce the ripple in the direct current and thus permit the frequency changer to operate at light load without discontinuities in the current wave. Another purpose is to reduce the rate of change of the current during a sudden large change in the a-c voltage, or during a fault on the inverters, thereby aiding the load regulators and the protection system.

Protection

Protection is provided against arc-backs in the power rectifier and arc-throughs in the power inverter, for either direction of power flow. Protection in the event of arc-back is provided by grid blocking (arc suppression) of the power rectifier, with tripping of the a-c circuit breakers as back-up protection. Grid blocking is effected by means of an electronic relay, which causes the application of a sustained negative voltage to the control grids. The arc-back is extinguished when the current of the anodes which fire to the faulted anode has dropped to zero.

An arc-through on the power inverter can recover itself within one cycle by

recommutation of the faulted anode by the succeeding anode, provided the fault current is below the commutation-limit value. Due to the action of the d-c reactor in reducing the rate of rise of current, most arc-throughs on the frequency changer are self-recovered, without operation of the protective equipment. Others are suppressed by grid-blocking of the power rectifier, without tripping of any circuit breaker. The grid-blocking circuit resets automatically in a fraction of a second, and the unit resumes normal operation. If the cause of the arc-through persists, such as a sustained fault on the grid-control circuit or a loss of voltage on the a-c system to which the power inverter is connected, the fault is cleared by automatic tripping of the d-c circuit breaker. The protective equipment operates only on the section of the frequency changer on which the arc-through occurs. Back-up protection is provided by the a-c circuit breakers, which are tripped by overload relays. The trip coils of the a-c circuit breakers are connected in parallel, so that operation of the overload relays on either side will trip both circuit breakers. Arc-back and arc-through protection of excitron rectifiers can be effected either by grid blocking or by interrupting the excitation arcs.

Installation and Operation

The a-c circuit breakers, the rectifier transformers, and the d-c reactors are located outdoors. The rectifiers with their auxiliaries, the d-c circuit breakers, and the control equipment are indoors. Each rectifier is cooled by a separate recirculating water cooling system, with a water-to-water heat exchanger. A control cubicle is provided for each rectifier. The indicating and recording instruments, the load regulators, the control switches for the circuit breakers, and the overload relays for the a-c circuit breakers are mounted on a station control switchboard.

The frequency changer operates with the cathodes of the power rectifiers grounded, irrespective of the direction of power flow. This makes it possible to mount the current-regulating and grid-blocking equipment without special insulation from ground. The change in direction of power flow is effected by means of double-throw switches in the rectifier control cubicles. These switches change the phase position of the grid-control voltages in relation to the anode voltages, and connect the load-regulating and grid-blocking circuits to the power rectifiers.

During an experimental period following the installation of the frequency changer, there were frequent outages. As a result of improvements in the rectifiers and the grid-control and load-regulating circuits, the performance during the last several years has been good. Outages are infrequent. Several arc-throughs per day occur on the power inverter, but are suppressed without tripping any circuit breakers. In case of an outage, the unit is returned to service immediately.

The efficiency of the frequency changer is approximately 93 per cent at full load, and the curve is fairly flat over the greater part of the load range. The power factor on the inverter side at full load is between about 82 and 84 per cent, depending on the direction of power flow. On the power supply side, the power factor is between about 85 and 95 per cent at full load, depending on the amount of phase control required by the power rectifier for load regulation.

Edgar Thompson Frequency Changer[35,36]

Power Systems

The 20,000-kw electronic frequency changer operating at the Edgar Thompson works of the Carnegie-Illinois Steel Corporation, near Pittsburgh, Pa., was placed in commercial service in December, 1943. Power may be exchanged in either direction between the steel company's 69-kv 60-cycle system, which is connected to a public utility, and their 44-kv 25-cycle system. The generation capacity of the 60-cycle system exceeds 600,000 kw, and the 25-cycle system has over 125,000 kw. The two power systems do not have a fixed frequency ratio; and they are both so large that a synchronous-synchronous rotating converter having the necessary torque to hold them in step would be prohibitive.

Power Circuit

The main power circuit diagram, Figure 37, shows that the electronic frequency changer has the following features:

1. There are two separately-operable 10,000 kw units, each consisting of two sections.

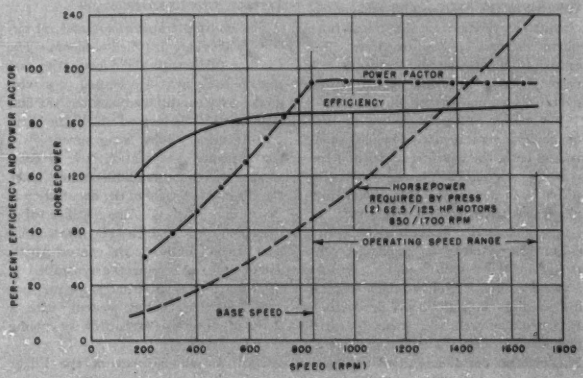
2. The frequency changer circuit has a d-c link, involving distinct power rectification, d-c interconnections and power inversion.

3. Sealed pentode ignitron tubes (Figure 6) were used,[32] which permitted the use of a double-way transformer circuit. Such a rectifier transformer is simpler and physically smaller than a single-way transformer.[27] If pumped ignitrons were used, they would have required transformers with single-way connection of the d-c windings, because their cathodes are interconnected by the vacuum pumps.

4. Both 60- and 25-cycle transformers have delta and wye a-c windings, so combined with zig-zag connected d-c windings, that an over-all 24-phase system is obtained. Current-limiting reactors are connected in series with each d-c winding, for limiting the fault current during an arc-back or an arc-through. The reactors are installed in the transformer tanks.

The frequency changer consists of several duplicate rectifier groups. When power flows from the 60-cycle to the 25-cycle system, the rectifying elements on the 60-cycle side operate as power rectifiers, converting a-c power to direct-current at 12,500 volts. The rectifying elements on the 25-cycle side operate as power inverters, converting the d-c power to a-c power into the 25-cycle system. Conversely, when the direction of power flow

Figure 35. Characteristics of adjustable-speed, electronic d-c motor drive



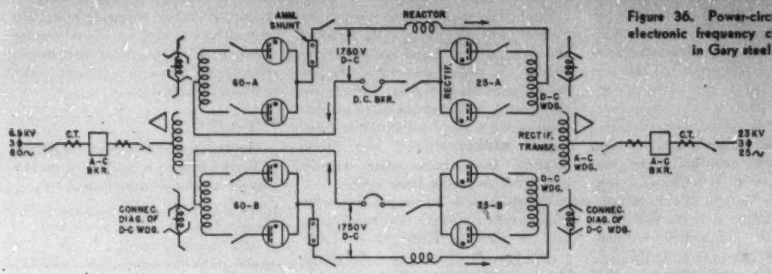


Figure 36. Power-circuit diagram of electronic frequency changer installed in Gary steel mill

is from the 25- to the 60-cycle system, the 25-cycle rectifying elements operate as power rectifiers and the 60-cycle units as power inverters.

Power Control

The installation is designed for unattended operation, although an observer is usually present in the building. It is controlled from remote panels located in a 60-cycle power station several hundred feet away. The operator can close the main a-c circuit breakers to place the frequency changer in service, and can select one of several methods of power control, as follows:

1. Power flow continually in either direction.
2. Adjust the rate of power flow, which will be maintained automatically at the set value.
3. Set the frequency changer to maintain automatically a desired rate of purchased 60-cycle power, within limits. This may involve automatic reversal of power flow direction, depending on generation and consumption of 60-cycle power within the company's own 60-cycle system.

The direction of power flow is established by the phase position of the supply voltages to the ignition and grid-control circuits, in relation to the transformer phase voltages of each rectifier. The change from one direction to the other is accomplished by means of contactors, operated by a master control.

The power transmitted by the frequency changer is regulated by phase control on both, the power rectifiers and the power inverters. The phase control is adjusted by means of saturable reactors in a phase-shifting network, which supplies the ignition and grid-control circuits of each rectifier group. An amplidyne, controlled by the power regulator, supplies a variable direct current to the magnetizing windings of the saturable reactors. Each rectifier group is provided with a phase-shifting network and an amplidyne. The voltages for the ignition and grid circuits of individual tubes in each group are obtained from the phase-shifting network through insulating transformers, because the tubes are insulated from each other.

Protection

Arc-backs in the power rectifier and arc-throughs in the power inverters are interrupted by arc suppression, through grid and ignition controls, without tripping the a-c circuit breakers.

When a fault occurs, instantaneously-operated thyratron tubes and backup relays block the conductivity in all the ignitron tubes of the 10,000-kw unit involved, by stopping ignition and biasing the grids negative. The current rapidly falls to zero; normal excitation is then reestablished automatically, and the unit resumes normal operation within about one-quarter second from the occurrence of the fault.

Protection for the rectifier transformers and backup protection for the tubes is provided by overcurrent relays, which are connected to current transformers in the a-c circuits and trip the oil circuit breakers.[21]

Installation and Operation

The rectifier transformers and oil circuit breakers are installed outdoors. The rectifier tubes with their auxiliaries and control equipment are housed in a windowless, air-conditioned building, the first floor of which is located above the main transformers. This arrangement was used because the location is in a flood area, and because available ground space was limited. A view of the installation is shown in Figure 38. The rectifier tubes are enclosed in cubicles on the second floor of the building. The control panels and the anode disconnects are installed on the lower floor. Each 10,000-kw unit has a recirculating cooling system, with a water-to-water heat exchanger, for cooling the ignitron tubes.[25]

The over-all efficiency of the Edgar Thompson frequency changer exceeds 90 to 93 per cent under most operating conditions. The power factor of the power rectifiers ranges between 85 and 95 per cent, and for the power inverters is between 75 and 85 per cent, depending upon the load and a-c system conditions.

The frequency changer operates usually in the 60-cycle to 25-cycle direction, but there have been instances when operation in the other direction was necessary. During some periods of power shortage on the 25-cycle system in the past year the converter was required to deliver power continuously at considerably above its full-load rating, and it performed successfully.

Life of the pentode ignitrons has exceeded expectations, and a majority of the original tubes are still in service. Most of the problems in this installation have been those of making correct adjustments of the automatic phase-control and temperature-regulating equipments. Before achieving the final correct adjustments, the frequency of faults was greater than desired by the designing engineers, but not excessive from an operating standpoint. A goal of not more than ten faults per day (24 hours) was set by the designers. This was eventually bettered, and under these conditions operation can be considered highly successful.

Frequency Changer for Induction Heating

Another type of electronic frequency changer, employing mercury-arc rectifiers, has been applied for converting a-c power from a polyphase system of a commercial frequency, such as 60 cycles, to single-phase power at a frequency of about 1,000 cycles, for induction heating and melting of metals.[26] The diagram of the power circuit is shown in Figure 39.

Power Circuit and Control

The frequency changer consists of a polyphase low-frequency rectifier trans-

former with a 3-phase zig-zag d-c winding, a single-phase high-frequency inverter transformer, and a 6-anode rectifier. The circuit consists of an induction heating coil with a parallel bank of capacitors *C*. The rectifier cathodes are connected to the neutral point of the rectifier transformer d-c winding through a d-c reactor. The transformer windings are represented by their voltage vectors. Each phase of the rectifier transformer is connected to a d-c winding section of the inverter transformer and a pair of rectifying elements. The rectifier transformer is connected to the a-c supply circuit through a circuit breaker.

The rectifier serves both as a power rectifier and a power inverter. The three pairs of rectifying elements operate as a 3-phase power rectifier, and each pair carries the direct current during approximately one-third of a cycle of the low-frequency voltage. During the conducting period of each pair, the two rectifying elements operate as a power inverter, and the current is switched between them every half-cycle of the high-frequency voltage. The high-frequency voltage required for commutating the current between the two rectifying elements of each pair is generated in the load circuit by oscillation between the induction coil and the capacitor. The actual operating frequency of the output voltage is determined by the constants of the load circuit. The power is supplied to the load circuit at a leading power factor, which is a requisite condition for the operation of a power inverter.

The control voltage for the grids of the rectifier is obtained from the output circuit. The output power can be regulated by phase control of the rectifier, with grid or ignition control at the low frequency (power-rectifier operation). A limited range of power regulation can be effected also by phase control at the high frequency (power-inverter operation).

The frequency changer is started by closing the a-c circuit breaker and applying a starting voltage to the control grids of the rectifier. A resistor is connected in the d-c circuit on starting and is short-circuited automatically when a high-frequency voltage is produced at the load terminals. While in service, the frequency changer can be stopped and started by push-button control without operating the a-c circuit breaker.

Some variations of the circuit in Figure 39 are possible, to permit using a high-frequency transformer with a single d-c winding. When sealed-tube rectifying elements are employed, they can be arranged in groups of three, with the high-frequency d-c winding connected between the cathodes of the two groups, and with the midpoint of this winding connected to the d-c reactor. The tubes of each group are connected to the three phases of the 60-cycle d-c winding. Another alternative connection is to divide the low-frequency d-c winding into two identical zig-zag sections, which are closely interlaced, and connect the high-frequency d-c winding between their neutrals.

When the rating of the unit requires twelve rectifying elements, the low-frequency transformer is provided with a 6-phase d-c winding, which improves the wave shape of the a-c line current.

Application

A number of electronic frequency-changer installations of this type have been made in the United States, for outputs of 250 to 600 kw. Most of them are used for supplying power to induction melting furnaces in brass and steel foundries. This type of frequency converter involves more equipment and more elaborate control circuits than the motor-generator type frequency changer used for induction heating. However, the electronic frequency changer has some notable advantages over the rotating type.

It consists of stationary equipment. It has a higher full-load efficiency (90 to 93 per cent), the efficiency curve is fairly flat over most of the load range, and the idling losses are low. The frequency is not fixed and can adjust itself to the load circuit, so that it is not necessary to switch capacitors when the load power factor changes, which occurs during a furnace melt, for example.

The application of this type of frequency converter is limited to the lower range of frequencies used for induction heating, because of the limitation imposed by the deionization time of mercury-arc rectifying devices.

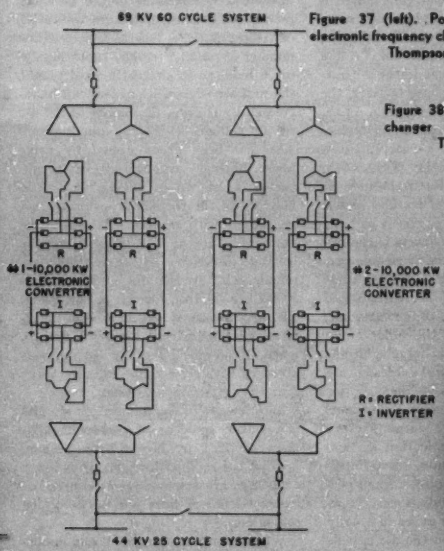


Figure 37 (left). Power-circuit diagram of electronic frequency changer installed in Edgar Thompson steel mill

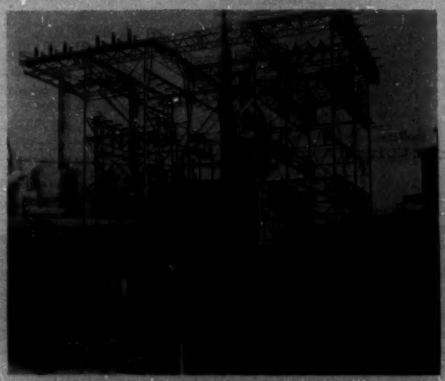


Figure 38. Electronic frequency changer installation in Edgar Thompson plant

Part VII. Inductive Co-ordination

Direct-Current Circuit

The d-c output voltage of a rectifier has ripples, because it is made up of portions of sine-wave voltages (see Figure 42). The ripple frequency is equal to the product of the a-c line frequency and the number of rectifier phases. The larger the number of phases the smaller is the ripple. Phase control of the rectifier, used for regulating the output voltage, increases the magnitude of the ripple. The voltage ripple can be resolved by a Fourier series into sinusoidal harmonic components.[1,24] The first harmonic has the same frequency as the ripple. The frequencies of the higher harmonics are multiples of this frequency. For example, the d-c voltage of a 6-phase rectifier supplied from a 60-cycle a-c system has harmonic components of 360, 720, 1,080, 1,440, and so forth, cycles. In general, the higher the order of an harmonic the smaller is its magnitude.

The harmonic components of the voltage cause the flow of harmonic currents in the d-c load circuit, the magnitude of any current harmonic being equal to the ratio of the harmonic voltage to the circuit impedance at the harmonic frequency. For most applications using rectifiers with six or more phases, the effect of these currents are negligible. Sometimes a reactor is connected in series with a 6-phase rectifier supplying electrolytic cells which have a high back-electromotive force and low resistance, such as zinc-producing cells or storage batteries, in order to reduce the magnitude of the sixth-harmonic current and the additional losses caused by it in the cells.

While the harmonic components of the rectifier output voltage and current usually are too small to affect the power-utilization equipment connected to the d-c circuit, they sometimes cause disturbances in communication circuits which have inductive exposures to the d-c load circuit. This has occurred on some street railway systems with overhead trolley wires or feeders and ground return. Harmonic currents were induced into adjacent telephone circuits, through capacitive or magnetic coupling, and caused objectionable noise.

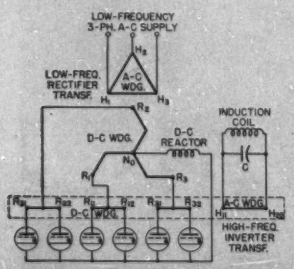


Figure 39. Power circuit of electronic frequency changer used for converting polyphase low-frequency power to single-phase high-frequency power for induction heating

Where such interference with communication circuits occurred, it was eliminated by filtering the output voltage of the rectifier, using a series reactor and resonant shunt filters as shown in Figure 40. Each branch of the shunt filter consists of a reactor L in series with a capacitor C which are in resonance at the frequency of the harmonic to be eliminated. The filter serves as a short circuit for the harmonic voltage, and the series reactor limits the harmonic currents through the filter. For 6-phase rectifiers, filter branches for the first three or four harmonics have been found adequate. One or two branches usually are sufficient for 12-phase rectifiers, when correction is needed.[8]

Alternating-Current Circuit

The a-c line current of rectifier units has a step-type wave shape,[1] which can be resolved into a sinusoidal component of line frequency and various harmonic components.[11,24] For most rectifier circuits, there are two harmonics in the a-c line current for each harmonic in the d-c output voltage. For a 6-phase rectifier, the a-c currents have the 5-th, 7-th, 11-th, 13-th, 17-th, 19-th, 23-rd, 25-th, (and so forth) harmonics. The a-c currents of a 12-phase rectifier have the 11-th, 13-th, 23-rd, 25-th, (and so forth) harmonics. The larger the number of rectifier phases the fewer harmonics are present and the higher is the order of the lowest harmonic.

The magnitudes of the harmonic currents have a definite relation to the magnitude of the total rectifier current and depend also on the order of the harmonic. In general, the higher the order of an harmonic the smaller is its magnitude. The use of phase control increases the magnitude.

Occasionally, the harmonic components of the rectifier a-c line currents cause interference in communication circuits exposed to the a-c circuits. Such interference has occurred in only a small percentage of rectifier installations. However, when the problem arises it usually is more involved than on the d-c circuit,

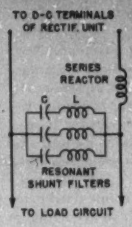


Figure 40. Filter for d-c output voltage of rectifier unit

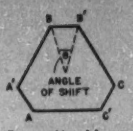


Figure 41. Vector diagram of phase-shifting autotransformer used for multiphasing rectifier installations

because of the larger number of harmonics and the greater extent of a-c systems. From the point of view of the a-c system, a rectifier installation can be considered as a generator of harmonic currents, which can flow into various branches of the system depending on their impedances. They do not necessarily have to flow to the power supply source only.

The probability of interference is practically nil for urban rectifier installations supplied by cable. The probability of an induction problem is greatest when a rectifier installation is supplied from a power system which has rural distribution lines supplying single-phase loads, and long open-wire telephone lines parallel the power-distribution lines.

Where there is a probability of interference, it is advisable to use as many rectifier phases as possible for the rating of the installation. In installations with a number of parallel rectifier units supplying a load, as in electrochemical plants, the number of rectifier phases can be increased by shifting the phases of the a-c supply voltages to the individual units.[12] This has been accomplished by using phase-shifting autotransformers connected as shown in Figure 41. The voltages to the rectifier transformer can be shifted with respect to the a-c line voltages by any angle θ, in the lagging or leading direction, by connecting terminals ABC to the line and terminals $A'B'C'$ to the transformer, or vice versa. The application of this method of multiphasing is illustrated in Figures 25 and 26, where a 36-phase rectifier system was obtained with six 6-phase units by using four 10-degree phase shifters.

Interference in communication circuits is less likely to occur on rectifier installations supplied by high-voltage feeders, because the rectifier line current is lower and there are fewer close exposures between telephone lines and high-voltage power lines.

When interference occurs, it can be cor-

rected sometimes by relatively inexpensive changes in the power or telephone systems. When such remedies are not possible or are too costly, the interference can be eliminated by using a filter on the line side of the rectifier transformer. Such a filter usually consists of reactors in series with the line and capacitors across the line, between the reactors and the rectifier transformer. The capacitors provide a low-impedance bypass for the harmonics, and the reactors impede their flow into the line. If the interference is caused largely by one or two harmonics, resonant shunts may be used instead of, or in addition to, capacitors. Such filters are usually fairly expensive.

When a rectifier installation is planned in a location where telephone interference might occur, it is advisable to make an investigation in advance, with the co-operation of the power and telephone companies and the manufacturer of the rectifier equipment. The probability of interference can be evaluated through such an investigation and remedial measures might be taken, if necessary, to avoid interference or to reduce its severity.

A comprehensive treatment of this subject is contained in reference 38 of the Bibliography. This report was prepared under the auspices of the AIEE electronic power converter committee. It includes relatively simple procedures, illustrated by an example, for evaluating the probability of interference from a-c systems supplying rectifiers.

Standards

A proposed standard for pool-cathode mercury-arc power converters was prepared by the AIEE electronic power converter committee and was published as an AIEE report in June 1946 (AIEE Number 6). It is expected to be issued in the near future as a standard by the American Standards Association.

The purpose of the standard is to establish uniform terminology for component parts and circuits of mercury-arc power converters, uniform symbols for circuit quantities, and standard methods of rating and testing. It includes sections on definitions, on standards for rating and tests, a test code, and sections on recommended practice, and on rectifier transformers. A table of rectifier circuits, with vector representation of the transformer voltages and standard terminal markings, is also included.

Conclusions

This paper has covered the present-day practices in the design and application of mercury-arc power converters in North America. In conclusion, some of the future trends that can be foreseen at present will be mentioned briefly. Such predictions are only estimates that could be upset by unforeseen developments in design or application requirements.

1. It is unlikely that pumped single-anode mercury-arc tubes, for a d-c rating greater than 1,000 amperes per tube, will be needed for some time to come. As the current per tube increases, the arc-drop voltage also increases and the efficiency decreases.

2. There is likely to be an increasing use of sealed tubes for still higher kilowatt outputs. Sealed tubes for current ratings higher than 400 amperes per tube will probably be developed.

3. There is likely to be an increasing application of mercury-arc converters for adjustable-speed motor drives. This application is still in its early stages, and has promising possibilities.

4. The use of high-voltage mercury-arc rectifiers for supplying power to radio-frequency oscillators, for induction and dielectric heating and for radio transmitters, is another promising application field.

5. Much has been written on the application of mercury-arc power converters for long-distance high-voltage d-c power transmission, particularly in European technical literature.[44] Experimental work on this application was carried on or is in progress in several European countries. An experimental installation has been operated also in the United States. Technically, such an application is possible, although considerable more development work is necessary. Its commercial possibilities on this continent will depend on economic factors and are unknown at present.

Appendix. Operation of Electronic Power Converters

The rectifying elements of an electronic power converter (Figure 44) can be considered as unidirectional switches, which connect the d-c circuit to each of the transformer phases, in cyclic sequence. The voltage of the d-c circuit, at any instant, is equal to the voltage of the transformer phase to which it is connected by one of the rectifying elements, minus the arc-drop voltage in the rectifying element, which will be disregarded in this discussion.

If the d-c circuit is connected simultaneously to two or more phases, the d-c voltage is equal to the average value of the instantaneous phase voltages, assuming the phase voltages and reactances are balanced.

Power Rectifier

In Figure 42 are shown the theoretical wave-shapes of the d-c output voltages for a 6-phase power rectifier with a simple star-connected transformer d-c winding, as shown in Figure 44. The positive half-cycles of the phase voltages are shown also.

Figure 42(*a*) represents the operating condition near no load, if the rectifier is permitted to conduct freely without delaying the anodes by grid or ignition control. Each phase is connected to the d-c circuit when its voltage is higher than that of the other phases, because an anode starts conducting when its voltage becomes positive to its cathode, and the common cathode connection is at the potential of the conducting phase. Phase 1 conducts until the intersection point *k* of its voltage with that of phase 2. Similarly, phase 2 conducts from *k* to *l*, phase 3 from *l* to *m*, and so forth. The conducting period of each phase is $2\pi/p$, in which p is the number of phases which conduct the direct current in succession. The voltage of the d-c circuit is indicated by the heavy trace in Figure 42(*a*), and has an average value E_{d0}.

In Figure 42(*b*) is shown the operating condition when the rectifier is carrying approximately full-load current. The anodes start conducting at the intersection points of the voltage waves as in Figure 42(*a*). However, because of the transformer reactance, a time interval is required for the transfer of the direct current from the outgoing phase to the incoming phase. During that interval, called the commutating angle u, both phases conduct current, and the voltage of the d-c circuit is equal to the average of the two phase voltages. The average of phase voltages 2 and 3 is represented by sine wave e_u, and the d-c voltage wave during the commutation period follows e_u. At the end of the commutation period, when the current of phase 2 has dropped to zero, the d-c voltage becomes equal to that of phase 3. The average value of the d-c output voltage, E_d, is lower than in Figure 42(*a*), because of the voltage reduction during the commutation periods. The com-

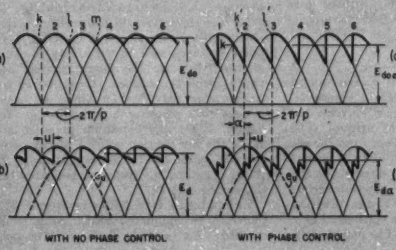


Figure 42. Theoretical wave shapes of d-c voltages of power rectifier

(*a*) No load, no phase control
(*b*) Full load, no phase control
(*c*) No load, with phase control
(*d*) Full load, with phase control

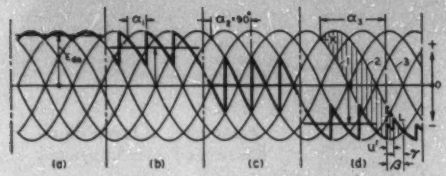


Figure 43. Phase control of rectifier d-c voltage, illustrating transition from power rectification to power inversion

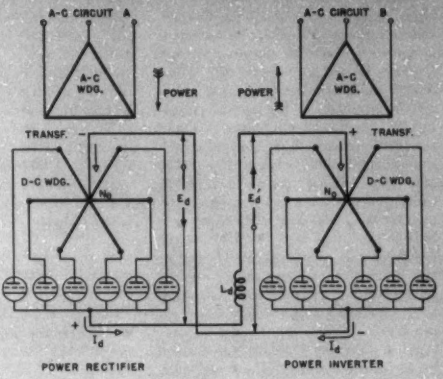


Figure 44. Diagram of connections of electronic frequency changer for interconnecting two a-c power systems

mutating angle u increases, as the load current or the transformer reactance (commutating reactance) increases.[1,24]

Phase Control

In Figure 42(*c*) is represented the operating condition, near no load, when the start of anode conduction is delayed beyond the intersection point of successive phase voltages. This delay is accomplished by grid or ignition control of the rectifying elements and is called phase control.

The rectifying element of phase 2 is permitted to start conduction at point k' of its voltage wave, which is displaced from the normal point k by the phase control angle of retard α. Phase 1 continues to conduct the current until phase 2 starts. Similarly, phase 2 conducts from k' to l', so that the conduction period is the same as in Figure 42(*a*). The average d-c voltage, $E_{d0}\alpha$, is lower then E_{d0}, due to the voltage reductions during the angles of retard. Phase control is used for controlling the d-c output voltage of rectifiers.

In Figure 42(*d*) is represented the condition when the rectifier is carrying the same load current as in Figure 42(*b*), while operating with phase control. The starting points of the anodes are the same as in Figure 42(*c*). After phase 3 starts at point l', phases 2 and 3 conduct simultaneously during the commutating angle, until phase 3 has commutated the current away from phase 2. During this period the d-c voltage wave follows the sine wave e_u, as in Figure 42(*b*). The commutating angle u in Figure 42(*d*) is smaller than in Figure 42(*b*), because the voltage difference between the phases during commutation is greater and the transfer of current is speeded up.

The average voltage $E_{d0}\alpha$ is lower than $E_{d0}\alpha$, because of the voltage reduction during the commutation periods. The reduction in the average voltage due to the commutating angle is the same as in Figure 42(*b*).[24]

Power Inverter

In Figure 43 are shown several stages of phase control, in which the angle of retard is varied from zero to some value over 90 degrees. The angle of retard is measured in each case from the intersection point of the positive half-cycles of the phase voltages. The rectifier is assumed to operate near no load, so that the commutation periods are not shown. When operating at higher loads, the voltage during commutation would follow the sine wave of the average voltage e_u, as in Figure 42.

In Figure 43(*a*), the rectifier is operating without phase control, as in Figure 42(*a*). In Figure 43(*b*), the phase-control angle is α_1, and the average d-c voltage is indicated by the horizontal line. In Figure 43(*c*), the angle of retard, α_3, has been increased to 90 degrees, and the average voltage is zero. It is assumed that there is sufficient inductance in the d-c circuit to maintain the flow of direct current even during the negative portions of the d-c voltage wave. If there were no inductance, the current could flow only during the positive periods and would be discontinuous. The average voltage then would remain positive up to a phase-control angle of 120 degrees.

In Figure 43(*d*), the phase-control angle, α_3, is greater than 90 degrees, and phase control is extended into the negative half-cycles of the phase voltages. The d-c voltage is negative; however, the current continues to flow in the same direction through the rectifier circuit. This is made possible by applying another voltage to the circuit to overcome the negative rectifier voltage.

The change in the direction of the d-c voltage, from positive values in Figures 43(*a*) and (*b*) to a negative value in Figure 43(*d*), while the direction of current remains unchanged, necessarily involves a reversal in the direction of power flow. In Figures 43(*a*) and (*b*), power from the a-c circuit is converted to power into the d-c circuit. In Figure 43(*d*), power from the d-c circuit is converted to power into the a-c circuit, and the rectifier unit operates as a power inverter. It should be noted that the change in the direction of power flow was effected by phase control, without any change in the circuit of the rectifier unit.

Frequency Changer

The operation of a power rectifier and a power inverter is illustrated in Figure 44. The directions of the voltages, current, and power flow are indicated by arrows. The power rectifier converts power from the a-c circuit A to direct current, and the output voltage E'_d is in the same direction as the direct current I_d. In the power inverter, the voltage E_d is opposite to the direction of current I_d, and it can be considered as a back-electromotive force. The power rectifier is connected to the power inverter so that the current flows in the same direction in both circuits, from anodes to cathodes. The voltage of the power rectifier overcomes the back-electromotive force of the power inverter to cause the flow of current. The d-c power output of the power rectifier is thus converted to a-c power into the a-c system B.

In the circuit of Figure 44, power is transmitted between two a-c systems which may operate at different a-c voltages and frequencies. Such a combination is called an electronic frequency changer. This circuit is also the basis of a d-c power transmission system, with one of the rectifier units located at the sending end and the other at the receiving end of the transmission line. The transmission voltage can be increased to any desired value by connecting in series a number of rectifier units or rectifying elements at each end.

The transmitted power can be regulated by regulating the output voltage of the power rectifier or the back-electromotive force of the power inverter by means of phase control. The direction of power flow can be reversed by changing the phase position of the conducting periods in the a-c voltage cycles of the two units. This can be accomplished by means of the grid or ignition control circuits of the rectifiers, without any change in the power-circuit connections.

Power-Inversion Limit

Referring to Figure 43(*d*), the operation of a power inverter is based on the ability to delay the conduction of the rectifying elements until the negative half-cycles of their phase voltages, and on the ability to commutate (transfer) the direct current between successive phases. Current can be commutated only from a phase of lower voltage to a phase of higher voltage. In Figure 43(*d*), this condition prevails between the intersection point K of the positive half-cycles and the intersection point L of the negative half-cycles, as indicated by the shaded area.

Commutation in a power inverter must therefore be completed before point L is reached. For example, if phase 2 does not

commutate the current away from phase 1 before point L, phase 1 will continue conducting beyond point L (when its voltage becomes higher than that of phase 2) and into its positive half-cycle. The inverter back-electromotive force will then disappear, and an inverter fault, called an arc-through, will result. Such a fault imposes a short circuit on the d-c power supply, through the faulted rectifying element and its transformer phase, and a high fault current can result. To avoid such a fault, the conduction of phase 2 must be started at a point on its voltage wave, which precedes point L by a sufficiently large angle of advance β, so that the commutating angle u' at the highest operating current is less than β. The cathodes of the rectifying elements are at the potential of the heavy trace in Figure 43(d). Between the end of commutation and point L, the anode of the rectifying element connected to phase 1 (which stopped conducting) is negative to the cathode. This negative period is needed for dionization of the rectifying element, so that its grid will regain control and keep the anode from firing when it becomes positive to the cathode after point L. This negative period is called the margin angle γ and is equal to $\beta - u'$. The minimum margin angle depends on the design of the rectifying elements.

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No Discussion

Radio for Telephone Service in America

H. I. ROMNES
MEMBER AIEE

IT IS a curious fact that the term "radiophone" was applied to the wireless transmission of speech many years before Marconi first demonstrated wireless. This came about when in 1880 Alexander Graham Bell sent speech over a light beam and Mercardier of France, in describing the experiment, called it the "radiophone." However, radio, as we now know it, got its start as a practical means of communication with the demonstration by Marconi. For the first 25 years, radio was used almost exclusively for telegraph but the possibility of using it for the transmission of speech was recognized very soon and within ten years speech transmission had been accomplished in the laboratory by several experimenters, both in America and Europe. However, the practical application of radiotelephony had to await the discovery of means for generating stable radio frequencies, for modulating them with speech and for amplifying the radio frequency energy.

The tool for doing all these things turned out to be the 3-element vacuum tube which was invented by de Forest in 1906 and developed into a successful amplifier, oscillator, and modulator during the 1912–1915 period. The door was then open to radiotelephony and results came quickly. Early in 1915, a vacuum tube transmitter of a few watts' power gave speech transmission over a distance of about 600 miles, using frequencies that ranged from 170 to 375 kc (1,800 to 800 meters). Later that same year, a transmitter employing hundreds of vacuum tubes in parallel to produce an output power of 2 or 3 kw was used to transmit speech from Arlington, Va., to the Canal Zone, the Pacific Coast, Hawaii, and Paris. In 1916, tests of 2-way radiotelephony were conducted with ships of the United States Navy. In 1917–18, fairly large quantities of radiotelephone equipment were produced for communicating with ships and airplanes and for other war purposes but radiotelephony became available too late to be of great use in World War I.

The first use of radio for regular public telephone service occurred in 1920 when a 2-way radiotelephone system was set up on the Pacific Coast between Catalina Island and Long Beach, Calif., a distance of 32 miles. This radio circuit was extended over wire lines to Los Angeles, Calif., and the rest of the Bell System. The system was used successfully for about three years. By that time, the growth in the telephone traffic to the island made necessary additional circuits in sufficient number to justify a submarine cable. Furthermore, the frequency band in which the radio system operated had been assigned to the rapidly growing broadcasting service.

The first practical demonstrations of high seas ship–shore radiotelephony also were made in 1920, but because the frequencies employed were needed for broadcasting and because of the postwar slump in shipping, these experiments were not carried to the point of providing commercial service.

Overseas Radiotelephony

After 1915 distance was no longer a limitation to talking by telephone over wire lines anywhere within the United States. The oceans, however, still remained a barrier to further expansion of the range of the telephone. Since radio offered hope of overcoming this last barrier to world-wide telephony, it was natural that early emphasis should be given to overseas radiotelephony.

The early experience with radiotelephony had demonstrated that the establishment of a reliable transatlantic radiotelephone circuit would require considerably more power than was practicable in the early 1920's. The development of higher power vacuum tubes was therefore undertaken and by about 1925 water-cooled vacuum tubes had been developed which permitted generation of many kilowatts of high power at radio frequencies. Also, the single sideband, suppressed carrier method of operation had been developed so that the available power could be used efficiently for telephony. Furthermore, means for interconnecting wire and radio circuits had been developed. Tests were then started with the British Post Office which resulted in a 2-way telephone circuit across the Atlantic Ocean being opened to the public on January 7, 1927. This circuit operated at 60 kc (5,000 meters) and is still available for use when transmission conditions at other frequencies are unsatisfactory.

The number of long-wave channels available is so limited that very little overseas service could be handled in this range. Furthermore, the long-wave system requires immense antenna structures and very high power, with the result that costs are so great as to discourage its use. However, it was soon discovered that short waves in the range of 3,000 to 30,000 kc could be transmitted over great distances. This opened up frequency space for an extensive increase in the number of radio channels. Furthermore, the use of short waves permitted moderate sized antennas which could be arranged to provide some measure of directivity and so reduce the amount of power required. These features made radio more economically attractive, permitting reductions in rates and resulting in rapid expansion of service.

The first short-wave radiotelephone circuit to London was opened for public service on June 1, 1928. In view of its success, circuits were soon established to

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H. I. ROMNES is radio engineer with the American Telephone and Telegraph Company, New York, N. Y.

Figure 1. Overseas radiotelephone service from the United States

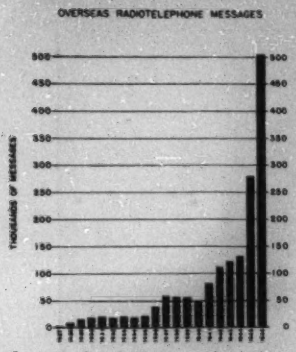

Figure 2. Growth in overseas radiotelephone traffic

other points in Europe and to South America, Central America, and over the Pacific Ocean. The present extent of the overseas radiotelephone network radiating from the United States is shown in Figure 1, and the growth in the number of messages and the reduction in rates is shown in Figures 2 and 3. There are now four overseas operating points in the United States. Through them, any one in this country can now talk to any country in the world having substantial telephone development, except for a few areas where communications are not back to normal since the war's end.

One end of a modern transatlantic radiotelephone circuit is shown diagrammatically in Figure 4. The connection from the subscriber, who may be located anywhere within the entire telephone system, appears at the overseas switchboard as a 2-wire telephone circuit. From there it passes to the transmitting branch of the radio circuit through a hybrid coil which prevents most of the incoming radio speech from going back out on the transmitting branch. Proceeding along the transmitting branch, the outgoing speech is regulated by a *vogad* (voice operated gain adjusting device) which brings it to practically constant volume of such value as to efficiently modulate the radio transmitter. The speech then actuates a *vodas* (voice operated device antisinging) which closes through the speech path to the transmitter and simultaneously blocks the receiving path during each actual speech spurt. This is necessary in order to prevent singing due to signals circulating around the radio circuit through the distant terminal when large gains are introduced to offset low volume. At this point in the circuit is shown one form of privacy device which is brought in to make the speech unintelligible to the casual listener. This is done by splitting the speech band into several smaller frequency bands which are then interchanged and some of them inverted to produce an unintelligible "scrambled" mixture. The "scrambling" combinations are changed rapidly in synchronism with similar changes at the distant terminal, in accordance with a prearranged schedule. The privacy device is transferred between the transmitting and receiving branches of the circuit by the *vodas* relays as required.

The "scrambled" speech from the privacy equipment passes either directly or through a channel shifter over a wire circuit to the radio transmitting station, which may be 40 miles or more away. The channel shifter is used in a multichannel radio system to displace channels from their normal frequency position with reference to the radio carrier so as to permit two or three channels to be transmitted simultaneously through a single transmitter.

The modern overseas radiotelephone transmitter is of the single sideband type in which only one sideband is used to transmit any particular speech band. In 2-channel single sideband systems, a different speech channel is carried by each sideband. Several of these systems at present are carrying three speech channels each and in these the third channel is divided between the two sidebands. In all cases, the carrier is transmitted at greatly reduced power, just sufficient to permit its being received at the distant terminal in enough strength to be used as a reference in the automatic volume control and automatic frequency control circuits.

The reduction of transmitted carrier, together with the fact that only a single sideband is used for any one channel, results in a net improvement in signal to noise ratio at the receiver of approximately nine decibels as compared with a double sideband circuit.

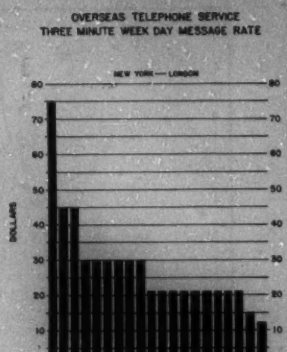

Figure 3. Overseas telephone service—reductions in rates

Rhombic antennas are used for the most part for both transmitting and receiving. These are of simple construction, have good directivity and operate efficiently over the entire frequency range used in any overseas service.

The received speech from the distant radio terminal is amplified and demodulated in a single sideband radiotelephone receiver. This receiver has arrangements for either simplifying the reduced carrier which was transmitted and then using this for demodulation purposes, or for using a local oscillator for this purpose. It also includes an arrangement for automatically tuning the receiver so that it follows accurately any slight variations which may occur in the carrier frequency of the distant transmitter. The receiver separates out the voice frequencies demodulated from the two sidebands, passes them through receiving *vogads* to produce

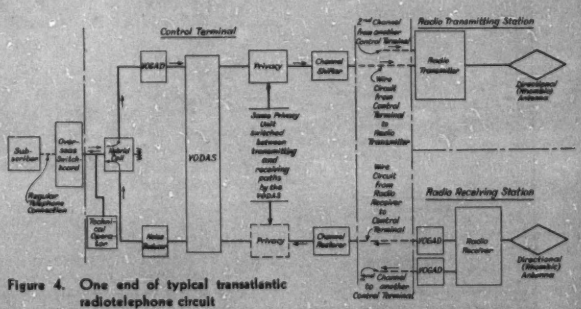

Figure 4. One end of typical transatlantic radiotelephone circuit

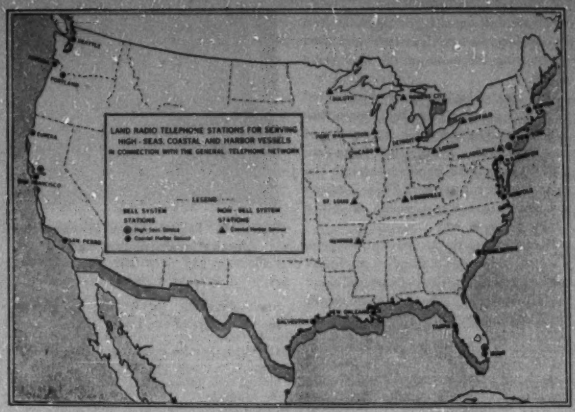

Figure 5. Land stations for serving high-seas, coastal, and harbor vessels

constant volume and then sends them over wire circuits to the control terminal, sometimes more than 100 miles away.

In the control terminal, the received speech passes through a channel restorer if required, then goes in succession through the privacy device for unscrambling, the *vodas*, a noise reducer which operates the suppress noise during pauses in received speech, and finally through the hybrid coil where it is recombined with the outgoing path to form the 2-wire circuit again at the overseas switchboard.

Mobile Radiotelephony

Radio is the only method available for communicating with mobile units at a distance, so that from the very beginning this use of radio has been emphasized. While radiotelegraph has been predominant in this field for most of the period since the inception of radio, in the last decade mobile radiotelephony has made vast strides as the opening up of new frequency ranges has made it possible to extend the service to more users.

Radiotelephone Service to Ships

Tests of radiotelephone service to high seas ships were made as early as 1916 with the United States Navy and in 1920 with commercial vessels. However, it was not until 1929, when the Leviathan was equipped, that telephone service to the public on ocean going vessels was established. For this service the shore station equipment is generally similar to that employed for short-wave overseas service. By the time World War II interrupted high seas service to the public, it had been made available on 39 of the largest liners on both the Atlantic and Pacific Oceans. The service has now been resumed on the Queen Mary, Queen Elizabeth, the S.S. America, and a few other of the larger ships, with the number of calls handled from each ship several times greater than was experienced before the war, sometimes running as high as 75 calls in a day from a single vessel.

The radio equipment for high seas service needs to be of a relatively high power and arranged for operation on several frequencies in order to give reliable service at various distances and at various times of the day. For vessels which operate within harbors or along the coasts, an arrangement can be used which is simpler and more economical both to install and to operate. Therefore, the high seas service from the United States has been supplemented by a comprehensive system of 15 coastal harbor stations placed at strategic intervals along the Atlantic, Gulf of Mexico, and Pacific coasts, as shown in Figure 5. These stations operate on frequencies in the two-to-three megacycle range, different frequencies being used for transmitting and receiving and a separate pair of frequencies being assigned for the different stations within a fairly large area.

A typical land station for the coastal and harbor service consists of a radio transmitter, usually of about 400 watts power, and several radio receivers remotely controlled from the switching center over wire lines. By providing receivers at several favorable locations along the coast, the transmitting ranges of the low powered ship sets and the higher powered shore transmitter are brought into balance. Typical transmitting and receiving stations are pictured in Figures 6 and 7. These are connected to the switchboard through an automatic control terminal which switches the system back and forth from transmit to receive as the conversation shifts from one party to the other. The control terminals also provide an automatic gain adjusting device to apply a constant volume to the radio transmitter and include arrangements for signaling ships and for receiving signals from ships.

A typical ship station consists of a radio transmitter and radio receiver connected to a hand telephone set having a push-to-talk key which causes the carrier to be transmitted when the key is depressed and the receiver to be connected when the key is released. Several sizes of sets are available, ranging in power from about 10 watts to 100 watts. Figure 8 pictures such an installation aboard a tugboat.

In making a call from a ship, the user simply picks up the hand set and pushes the key momentarily. This sends out a spurt of carrier which is picked up by one or more shore receivers to operate relays which light one or more lamps at the switchboard. The operator plugs into a jack associated with one of the lighted lamps. This operation automatically

Figure 6 (left). Coastal-harbor radiotelephone transmitting station

Figure 7 (right). Coastal-harbor radiotelephone receiving station

Figure 8. Radiotelephone installation aboard tugboat

selects the associated receiver, starts the shore transmitter, and places the system in the transmitting condition. The operator answers the call by saying, for example, "This is the Norfolk, Marine Operator." The ship user then depresses his push-to-talk key, which sends out carrier to operate a relay in the shore control terminal to switch the system to the receiving condition, and gives his instructions to the operator who completes the connection over the regular telephone system in the normal manner.

To make a call from a shore telephone to a ship, the shore subscriber reaches the marine operator over the regular wire telephone network, giving her the information as to the ship being called. The operator plugs into the jack associated with the system and, if the called ship is equipped for selective signaling, dials the code for the ship being called. This sends out alternate pulses of 600 and 1,500 cycle tones which operate selectors on all ships within range which are equipped for selective signaling, but only the selector whose code is dialed will close a contact to ring a bell. For ships not equipped with selective signaling the operator calls by sending an attention tone and then announcing the name of the ship being called.

The coastal harbor service has proved to be very popular, there being more than 5,000 ships registered for the service at the end of 1946. During 1946, more than 200,000 calls were handled through the coastal harbor stations. These stations also are being used to a limited extent for service to airplanes.

In addition to the 15 stations along the seacoast, there are a number of similar stations around the Great Lakes and along the largest rivers. These, also, are shown in Figure 5.

General Mobile Service

As long as radio was limited to the lower frequencies, the number of channels available for mobile service was so small that they were reserved largely for use where safety of life was involved, for example, to ships at sea. During the 1930's, the usable frequency range was gradually pushed upward beyond 100 megacycles. Not only did this increase the number of frequency bands available but because of the lack of skywave transmission at the higher frequencies, the assignments could be reused at fairly short geographical spacings. In the 1930's therefore, it became practical to extend mobile radio to police and fire departments, to public utilities and to other emergency applications.

World War II accelerated the development of mobile radiotelephone equipment and the further extension of the usable frequency range. It now became possible to assign a little frequency space for other than emergency services. When the war ended, a few frequencies were made available for a trial of general mobile service of two types:

Figure 10. Radiotelephone transmitter and receiver installation in automobile

1. Urban service operating in the 152–162 megacycle range and intended to cover a limited area, usually a city and its surrounding suburbs.

2. Highway service operating in the 30–44 megacycle range and intended primarily to serve vehicles or other mobile units traveling on highways or waterways between cities.

For highway service, stations are placed at intervals of about 50 to 100 miles, depending upon terrain, so as to give continuous coverage along the more important highways. The distinguishing feature of "general" mobile service is that it permits connection to be made from a mobile unit to any telephone in the worldwide network. This requires considerably different arrangements than are used for private radio systems where connections with mobile units are established from only one or a limited number of fixed points.

In the three years since the end of the war, general mobile service has been established in more than 50 cities in the United States and more than 100 systems are under construction or being actively planned. Several of these cities now have two or three stations in service. Each of these stations serves 50 to 100 mobile units, depending upon the types of calls involved. The location of the stations now in service or under construction are shown in Figure 9.

A typical station for mobile service includes a 250-watt frequency-modulated radio transmitter with its antenna on a high building or other elevation so as to provide good transmission over the area to be served. Several fixed receivers are placed at favorable locations within this area to pick up the relatively weak signals from the lower powered mobile transmitters. The transmitter and the several receivers are connected to a switchboard through a control terminal which provides the means for bringing together the two 1-way radio paths, for automatically

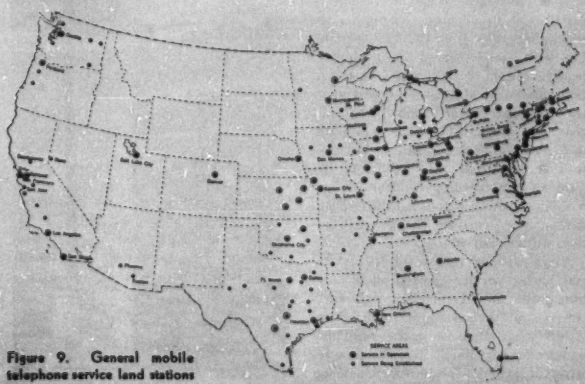

Figure 9. General mobile telephone service land stations

adjusting the volume to a constant value for application to the radio transmitter, for operating a switchboard signal lamp on incoming calls, for signaling particular cars on outgoing calls and for automatically checking that the transmitter is operating properly and within the required frequency tolerance. Also, arrangements may be included for automatically selecting the one receiver which is producing the best signal-to-noise ratio and cutting off the others so as not to introduce unnecessary noise. At the switchboard, connection can be made to the regular telephone network as is the case with any subscriber's line.

A typical mobile installation consists of a frequency modulated transmitter of 20- to 40-watts power and a receiver, both placed in the luggage compartment of the automobile, as shown in Figure 10; an antenna about 18-inches long mounted in the middle of the metal roof of the automobile (Figure 11); and a hand telephone set, together with the signal bell and lamp, located below the instrument panel convenient to the automobile driver (Figure 12). To place a call the mobile subscriber simply picks up the hand set and operates momentarily the push-to-talk key in the handle of the set. This causes carrier to be transmitted which is picked up by one or more of the fixed receivers, causing the switchboard lamp to light. The operator answers the call and makes the connection requested by the mobile subscriber. The mobile user presses the button when he talks and releases it when he listens, but the user at the fixed end of the connection uses his telephone just as he does for an ordinary wire connection.

Figure 11. Automobile antenna for urban mobile radiotelephone service

A call from any fixed telephone in the network to a mobile unit is made by obtaining the mobile operator in the usual way and giving her the telephone number of the car being called. The operator dials this number which causes alternate pulses of 600 and 1,500 cycles to be sent out, thereby operating the selector unit associated with the receivers of all mobile units within range of the transmitter but only the selector adjusted for the code being dialed will close a contact to ring a bell and light a lamp in the desired vehicle.

The general mobile service has filled an important need, the demand already having exceeded the number which can be handled over presently available frequency assignments in the larger cities. It has been estimated that a large metropolitan area like New York might require 150 channels, each handling about 75 vehicles, to take care of the demand for mobile service over the next few years. A few examples of the types of customers who are using mobile service are ambulances, automobile service companies, small boat operators, burglar and fire alarm services, public utility companies, police and fire departments, newspapers, physicians and trucking services. Systems using the same types of equipment and operating in the same frequency range are also used to serve vessels in harbors, lakes, and rivers.

Telephone service to passengers on railroad trains was started on August 15, 1947 with several trains on the New York-to-Washington run equipped for the service. Initially the service is being given through the general mobile systems located in the several large cities on this route. The arrangements on the train differ considerably from those generally used in automobiles in that the push-to-talk switch is eliminated in order to make the service as easy to use as a telephone in one's home. This requires that the radio transmitter and receiver in the train be operated simultaneously which is accomplished by using separate antennas for transmitting and receiving and introducing a radio frequency filter in the receiver to discriminate against the transmitting frequency.

Consideration is being given also to providing telephone service for passengers on some airplanes. This involves special problems in that the plane travels so very rapidly and, because of its elevation, may cause interference to receivers located hundreds of miles away.

Domestic Point-to-Point Radiotelephony

The use of radiotelephone for point-to-point communication within the United States until recently has been on a relatively small scale. As long as radio was limited to the lower frequency ranges, the small number of channels which were available had to be reserved for mobile and overseas services and other situations where wire telephony could not be employed. As new frequency ranges were opened and vastly larger numbers of channel assignments became available, the application of radiotelephony could be extended from situations where wire telephony was impossible to cases where it was impracticable, then to cases where it was difficult and, perhaps, in the future to situations where wire is less economical.

Emergency Radio

The use of radiotelephone for emergency point-to-point communication when wire lines are disabled by storm or flood, came early in the foregoing scale of radio applications. For this purpose about 100 portable transmitting and receiving stations have been made available and these are stored when not in use at strategic points throughout the United States. These sets operate on a single frequency in the 2-megacycle band and employ a transmitter having a power output of about 50 watts. The receiver is normally connected to the antenna, but when speech is applied from the wire circuit, a voice-operated relay switches the antenna to the transmitter and energizes the transmitter. Arrangements are included to permit connection to various types of wire line and to handle several types of signaling. Generally each of these emergency sets is mounted in a small trailer along with all arrangements necessary to make it a self-contained unit, including a gasoline-driven generator to provide power. The trailer is light enough so that it can be pulled by an ordinary passenger car and can be moved by hand for

Figure 12. Telephone set and control unit in automobile

Figure 13 (left). Emergency radiotelephone installation in trailer

Figure 14. Antenna installation for experimental rural radiotelephone installation at a ranch

short distances to obtain desirable final locations. Figure 13 shows one of these trailer-mounted emergency sets in operation.

An illustration of the use of this equipment was the occasion when the communities of Denison, Whitesboro, and Gainesville in north central Texas were isolated from the telephone network by an ice storm which destroyed the wire lines. Two of the emergency radio systems were quickly placed in operation, and some 640 telephone messages were handled in this manner during a 6-day period while wire circuits were being restored. A number of such uses occur each year.

Point-to-Point Radio in the 30 to 300 Megacycle Band

In 1934 a radiotelephone installation operating in the 60-megacycle region was installed between Provincetown on Cape Cod and a point near Boston. A few additional single channel systems have since been installed to reach islands and other points which are inaccessible to wire lines.

In 1941 a 12-channel system operating in the 160-megacycle range was installed between Ocean View and Cape Charles to provide circuits terminating at Norfolk from points on the eastern shore of Chesapeake Bay. This 25-mile radio link was an alternative to providing wire circuits which would go around Chesapeake Bay, a total distance of over 200 miles, or submarine cables subject to frequent damage since they would cross a very busy ship channel.

An interesting recent use of radio for point-to-point service and one of the first applications of frequency modulation for this type of service, was in Death Valley, Calif. This region is isolated from inhabited areas by vast stretches of deserts. Telephone service had previously been provided over a wire line along a railroad which was used to haul out the borax which was mined in Death Valley. This railroad has been abandoned, with the result that the wire line is inaccessible for maintenance purposes. Radio systems, therefore, were used to connect Death Valley to an important open-wire line which passes about 25 miles away. They operate in the 150-megacycle range and each system can provide three telephone channels.

Another use of point-to-point radio is an experimental system to serve a group of isolated cattle ranches in Colorado. One end of the radio circuit is located at a small central office and this provides communication to eight ranches on a party line basis, radio equipment being located at four of these ranches and the others being connected to one of the radio stations by wire extensions. The antenna installation on a ranch is shown in Figure 14. This equipment operates in 50-megacycle range and is arranged to operate from the customer's standpoint just as though the telephones were served by rural wire lines. During a severe snow storm in its first winter of use this radio system proved invaluable in obtaining emergency help.

Microwave Point-to-Point Systems

As has been previously outlined, radio started in the early 1920's with frequencies as low as 60 kcs. Step by step the art

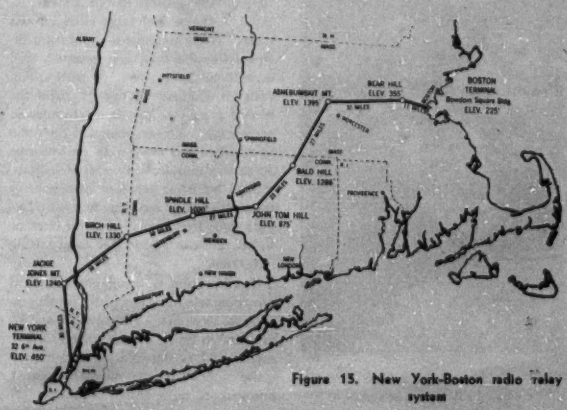

Figure 15. New York-Boston radio relay system

Figure 16. Repeater station in New York-Boston radio relay system, showing lens antennas

advanced into the higher frequency ranges and reached about 20 megacycles in the late 1920's and went on beyond 100 megacycles in the 1930's. Each advance has greatly increased the number of channels available and has brought radio nearer to wire lines in transmission performance. With the most recent advance into the region of thousands of megacycles, radio energy can be directed along narrow paths, atmospheric and man-made noise is very low or nonexistent, the variation in transmission loss is sufficiently limited that it generally can be compensated for by regulating arrangements, and large numbers of channels are available. These characteristics so nearly approach those of wire that it is possible that in the future radio will be used in many cases where wire has previously had a clear field. Also, many of the techniques which have been devised to extend wire transmission over great distances, such as intermediate amplifiers at regular intervals to overcome path losses, can be employed for radio operating in these frequency ranges.

Catalina Island, which saw the first commercial application of radiotelephony, has again become involved in another pioneering use of radio. During 1946 an 8-channel pulse modulated radiotelephone system operating in the 4,500-megacycle range was installed between Los Angeles and Catalina Island, a distance of about 50 miles. The radio beam is sharply directed by means of parabolic antennas, permitting the use of transmitting power of only a few watts. A similar system has been installed between the mainland and Nantucket Island on the east coast of the United States and a 2-link system providing 16 channels by the same method has been placed in service to span a distance of about 115 miles in very mountainous country between Marysville and Redding, Calif.

In order to determine the field of use of microwave radio in the commercial communication network of the United States, a full-scale radio relay system has been set up between New York and Boston, a distance of more than 200 miles. This trial system employs seven intermediate radio relay stations with a clear line-of-sight path between adjacent stations. Two radio channels are provided in each direction, one for service and one standby. Frequencies in the vicinity of 4,000 megacycles are used and the frequency used for a particular channel is shifted by 40 megacycles in passing through a repeater station. For a given channel the same radio frequency is used in alternate repeater sections. In the repeater most of the amplification is provided at an intermediate frequency of 65 megaycycles, but the final amplifier operates at the radio frequency of about 4,000 megacycles and employs velocity variation tubes. The transmitted power is of the order of one watt and this power is directed toward the adjacent station by means of a shielded lens antenna having an aperture ten feet square to focus the radio energy into a beam sharper than that produced by a searchlight. Each of these antennas provides a power gain of more than 8,000-fold (39 decibels) over an isotropic radiator. Each radio channel provides a communications channel four or five megacycles wide, and this broad channel can be used for television transmission or for hundreds of telephone channels. Various methods of using this broad channel for many telephone circuits are being tried out, including frequency division and time division.

Figure 15 shows the location of the repeater stations between New York and Boston and Figure 16 pictures one of these stations, with the shielded lens antennas showing prominently. One antenna is used for transmitting and one for receiving in each direction of transmission. The same antenna is used for two channels and it is possible by means of band pass filters to combine still more broad channels for transmission through the same antennas. In addition to the radio equipment, the relay station includes emergency power equipment which functions to keep the station in operation even though the outside source of power should fail.

Plans are well under way for similar radio relay systems from New York to Chicago and along other routes.

When Marconi demonstrated radio 50 years ago, its first application was for transmitting intelligence by means of a telegraphic code consisting of a series of short pulses. Since that time many other methods of using radio for the transmission of intelligence have been developed and applied, but recent developments again use a code made up of short pulses, this time for the transmission of speech. In one system of pulse code modulation, samples of a speech wave are taken 8,000 times a second and each sample is translated into a 7-unit code which carries over the radio circuit information as to the magnitude of the sample, thus permitting the speech wave to be reconstructed at the receiving end. Systems have been built in the laboratory in which information as to samples from 100 different speech channels may be transmitted over the same radio channel on a time sharing basis. The advantage of such a system is that there can be great disturbances, variations, and distortions in the transmission medium without affecting the quality of the speech, since it is only necessary to determine at the receiver whether the signal is present or absent, with no gradations in between, in order to perfectly reconstruct the original speech signal. Furthermore, the signal can be regenerated just as telegraph signals are regenerated, thus permitting transmission through many relay stations without degradation. It is interesting that in the past 50 years we have made a complete circle in coming back to something like the telegraphic code used by Marconi, except that now we send millions of signal pulses each second instead of tens of pulses. During only a little more than half of the 50-year interval since Marconi's celebrated demonstration has the medium of radio been utilized for commercial telephone purposes. Not only has the length of individual radiotelephone links increased enormously since the establishment of the short 32-mile link between Catalina Island and the California mainland, but also the variety of places reached by radiotelephone has been multiplied. Much opportunity for development clearly lies ahead in this field, and the part that radio will play in world communications 50 years hence is indeed a fascinating subject for speculation.

No Discussion

The Bellingham Hydraulic Log Barker

V. C. HANER
NONMEMBER AIEE

H. A. ROSE
MEMBER AIEE

IN THE PRODUCTION of the higher grades of wood pulps, used in the manufacture of paper and other products, it is necessary to remove the bark from the logs as the initial process in the preparation of the wood for manufacture into pulp.

Phenomenal savings in valuable pulp wood and labor are being realized by the Pacific northwest pulp and paper mills, Figure 1, through the use of modern full-sized-log debarking machines. The larger of these machines are capable of debarking run-of-the woods sized logs of to 80 inches in diameter "bucked" to lengths approximating 20 feet. So successful has been the experienced gained with the latest type of these large size debarkers in handling even the small logs that increased attention is presently being given to the building of smaller units designed specifically for mills requiring less production or where short lengths, small diameters, or "farmer wood" may prevail.

The older methods of debarking small cut-up sections of the tree, by tumbling them together in revolving drums or by passing the sections under the knives of high-speed barking machines is rapidly becoming obsolete. Modern northwest wood preparation plants utilizing large full-size-log hydraulic barkers and chippers[1] are realizing savings in valuable pulp wood in the order of 20 per cent over the older wasteful methods. It is expected that increasing use will be made of still larger hydraulic barkers in large saw mills for removing the bark of the logs prior to their reaching the head saw, to improve the yield in lumber, conserve by-product pulp wood and to reduce saw maintenance and mill clean-up expenses.

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V. C. Haner is plant engineer with the Puget Sound Pulp and Timber Company, Bellingham, Wash., and H. A. Rose is consulting and application engineer with the Westinghouse Electric Corporation, Seattle, Wash.

Types of Big-Log Hydraulic "Barkers"

This paper will deal primarily with the electrical engineer's problems, applying to the new currently known Bellingham-type of big-log hydraulic barker developed by the engineers[2] of the Puget Sound Pulp and Timber Company of Bellingham, Wash. It is felt however that brief mention and review should be made of the other three principal types of big-log hydraulic barkers which preceded the Bellingham type during the past five years, all of which are presently in successful operation in mills of the Pacific northwest.

All of these barkers make use of a large quantity of high-pressure water directed against the log by a suitably-designed nozzle or system of jets. By appropriate electrical controls the nozzle and log are each moved with respect to the other so that the full area of bark is covered. The force of the powerful jets blast and wear away the bark, leaving a clean wood surface. An operator called the barkerman is required to manipulate the various controls whereby the logs are loaded into the machine where they are barked and finally unloaded or removed from the machine.

The Lathe-Type Barker

In this barker the log is chucked between centers of a lathe-type arrangement by which rotary motion is imparted to it. The nozzle which is positioned from above is held by the force of gravity at correct distance from the log by a shoe which rides on the bark. As the log turns the nozzle is caused to move along the log axis and the bark is removed in a way similar to the operation in a simple wood lathe.

The Log Indexing Barker

In this barker the log is firmly held by a surrounding collapsible set of chains which grips and imparts to it either an indexing or slow continuous rotational movement. The nozzle is positioned under the log and is rapidly reciprocated for the full length of the log. The log has been barked by this continuous stripping action when it has made one complete revolution.

The Adjustable Ring Barker

In this barker the logs travel butt to butt through an adjustable diameter ring-type segmental nozzle designed to cover the periphery of the log. Water is released at the instant a log enters the ring and is shut off as soon as the trailing end has passed through. For logs larger than a certain diameter the segmental nozzle does not make complete coverage which results in strips of bark being left. To remove these, the log is indexed or turned the required amount and reversed through the machine.

The Bellingham Barker

In the Bellingham barker the logs are supported in and caused to revolve by two adjacent sets of trunnion rolls, Figure 2. A nozzle supported from above is driven along the axis of the log as it turns. The jet removes the bark in a manner quite similar to that in the lathe-type barker. Figures 3 and 4 show the principal elements and layout of a typical Bellingham type of barker plant.

The logs as received from the pond, Figure 5, are first cut to length by the cut-off saw, Figure 6, ahead of the barker. From the saw they are passed to the fore deck at the entrance of the barker by approach chains under the control of the barkerman. They are prevented from entering the trunnion rolls by stop arms which form a part of the loader system. The approach chains may operate more or less continuously by slipping under the logs as held back by the loader.

Figure 1. The log pond, wood mill, and chip silos, Puget Sound Pulp and Timber Company, Bellingham, Wash., showing entrance to the log slip

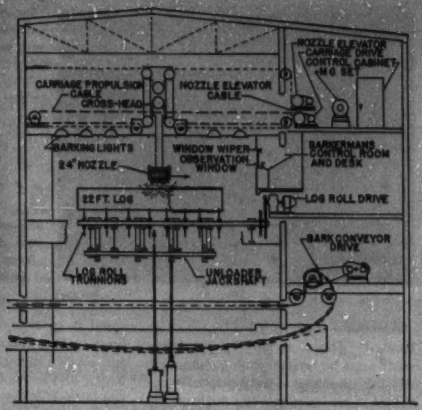


Figure 3. Typical layout of Bellingham-type barker plant

View facing side of log

Assume that the barkerman has placed all auxiliary machinery in operation preparatory to barking. He first starts rotation of the empty trunnions at a speed suitable for the particular log held at the loader. By a thumb button on one of the control levers of the control desk, Figure 7, he causes the unloader to move to the raised position presenting the unloader sectors as knees to the incoming log. If necessary, he elevates the nozzle by a "raise" thumb button to a height such that it will not be struck by the incoming log. He next operates the loader which simultaneously starts a log rolling into the trunnions and throws a block against the release of other logs. The loader is then allowed to retrieve, permitting advance of the next log to be barked.

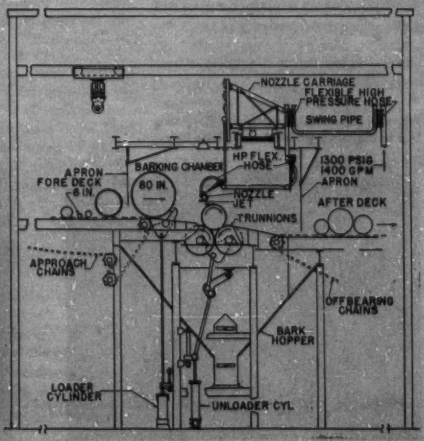
The entering log is prevented from rolling past the trunnions by striking the knees of the unloader. It immediately starts to accelerate to trunnion speed as the barkerman retrieves the unloader. He next lowers the nozzle if this is necessary, by appropriate thumb button. He has previously estimated and set the approximate speed at which the nozzle is to traverse the log. By operation of a "forward" thumb button on the nozzle control lever the nozzle is caused to traverse the log. It automatically stops at the opposite end. The barking of this log is now complete. He again energizes the unloader which ejects the barked log to the after deck, see Figure 8.

If in the meantime he has transferred his controls for "automatic loading" another log will be automatically started into the trunnions and the procedure will be repeated but with reverse travel of the nozzle carriage. These and other operations are built into the barkerman's controls in such a way that minimum effort is required to manipulate them. Proficiency is mastered after a short training period so that several hundred logs can be easily barked in an eight-hour shift.

Figure 2. Interior of the barking chamber showing trunnion rolls, loader, high-pressure flexible water hose, nozzle, and barkerman's observation window

Figure 4. Typical layout of Bellingham-type barker plant

View facing end of log



Barker Sizes and Barked Log Production

The size of the Bellingham barker is determined principally by the diameter

Figure 5 (left). Fifty-inch diameter log on log slip on the way to the cut-off saw

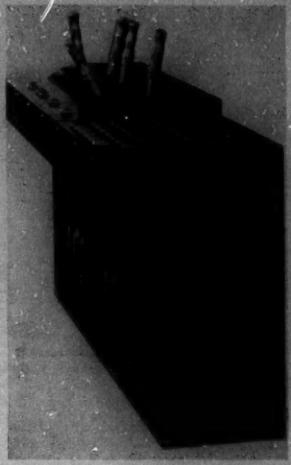


Figure 7. Barkerman's control desk showing operating levers and auxiliary equipment push button control section

and length of the largest log that is to be handled. The V formed by the trunnions must be large enough to maintain stability when logs of usual deformity revolved at moderate speeds. Eighty-inch logs require about 48-inch diameter trunnions. Sixty-inch logs can be handled in 36-inch trunnions. The number of trunnion rolls must be sufficient to afford reliable turning effort for deformed and crooked logs, and arranged so that short logs can be barked at full pressure without too great a tendency to up-end from the force of the jet.

The production or output of the barker depends upon the speed of log rotation and the length of the nozzle jet. The nozzle can advance no more than one nozzle length per revolution of the log, otherwise threading will result in bark being left on the log. Overlapping may in certain cases be required when bark is difficult to remove, but in all other cases it represents a loss of production. In practice a certain degree of overlapping occurs at all times which is partly compensated for by the natural tendency of the jet to widen or flare in passage to the log. About 500 feet per minute of log surface speed is considered an upper practical limit for log rotation. With about 75 gallons per minute per inch of nozzle length, and in the order of 1,200 to 1,300 pounds per square inch water pressure, very satisfactory barking action is obtained for all species and conditions of Pacific northwest trees.

Based on perfect co-ordination between revolutions per minute of the log and nozzle advance, the production or output of the barker is:

$$\text{Production in cubic feet per hour} = 0.104\ RLDE$$

where

R = surface speed of log in feet per minute
L = nozzle length in inches
D = mean diameter of log in inches
E = barking efficiency
S = free running nozzle speed in feet per minute

The efficiency factor E recognizes the impossibility of maintaining the jet at 100 per cent barking speed S and accounts for all time lost in accelerating and decelerating the carriage, rebarking loading and unloading, overlapping and so forth. For the usual application E may be taken as 0.5.

For any nozzle speed less than the critical speed the production is:

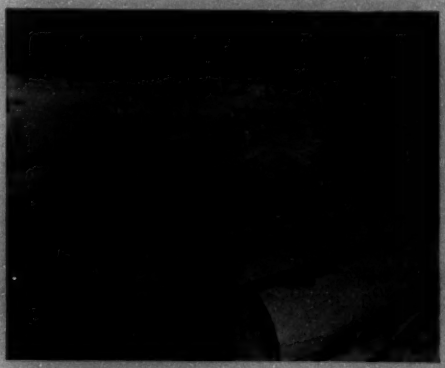
$$\text{Production in cubic feet per hour} = 0.328\ D^2SE$$

For short lengths and small log measure it is sometimes desirable to refer to the barker output in terms of stacked cords. If the relation between solid wood and stacked volume for the cord is taken as $\pi/4$, the solid cubic contents of the cord becomes $4 \times 4 \times 8 \times \pi/4 \cong 100$ cubic feet.

Figure 8. Barked logs at the exit of the barker on way to whole log chipper

Dark spots on logs are wood stains

Figure 6. Nine-foot circular cut-off saw cuts logs to proper length for the hydraulic barker



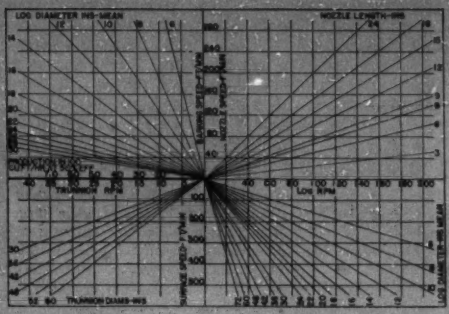


Figure 9 (left). Production chart for Bellingham barkers having different nozzle lengths, trunnion diameters, and speeds

Figure 10. A 24-inch 1,300 pounds per square inch nozzle and jet removing bark from a 20-foot log

Figure 9 relates the various production factors discussed and gives the barker output in 100's of cubic feet per hour≅solid cords per hour, at an assumed nozzle speed-efficiency E of 50 per cent. The third quadrant is for relating the log and machinery speeds and includes the full range of log sizes encountered. The first quadrant is for determining production output and includes only the smaller log diameters. Scaling of many thousands of logs delivered to the mills shows that for run-of-the-woods logs, the arithmetical and/or weighted average size will be in the range of 15-to-20 inch diameter.

Figure 9 gives barker production in solid cubic feet measure. The values as read from it may be converted to Brereton[1] scale-board feet measure by multiplying the number of cubic feet produced by 12. To obtain the corresponding Scribner decimal C, Spaulding, and British Columbia[3] scale-rule values it will be found sufficiently accurate for barker application purposes to take 60 per cent of the calculated Brereton values.

Water Supply for the Barking Nozzle

Basically, bark removal is brought about by the combined effects of erosion and blast from the impinging force or jet action of water at high velocity applied to a small area of bark surface, see Figure 10. Within the range of effective barking pressures and practical log rotation speeds, the bark removing ability of the jet can be taken to vary as the product of mass and velocity per unit length of nozzle. In practice these quantities are usually expressed as flow in gallons per minute per unit length of the barking nozzle (representing mass) and the square root of the orifice pressure in pounds per square inch (representing velocity).

The quantity of water required by a particular nozzle for effective barking will depend upon its design. In general, slot-type nozzles, Figure 10, having about 50-thousandths-inch slot width require from 65 to 80 gallons per minute per inch at a working pressure of 1,100 to 1,300 pounds per square inch.

A 50/1,000-inch nozzle delivering approximately 80 gallons per minute per inch at 1,300 pounds per square inch will remove the most difficult bark from northwest logs, as occasioned by cutting the trees during the dormant winter months.

Multi-stage high-pressure induction motor driven 3,570-rpm centrifugal pumps, Figure 11, are used to feed the nozzle. Small 6-inch nozzle barkers require about 500 horsepower to drive a single pump. A 24-inch nozzle will require at least two 800-horsepower pump sets. Induction motors of this type and speed will take about 550 per cent of full load kilovolt-amperes when started at full voltage across the line. For mills having somewhat limited capacity, reduced voltage starting may be required to minimize disturbance to other equipment.

Steam turbines make very desirable and economical drives for these pumps when the heat balance of the mill justifies their application. Turbines have an advantage of adjustable speed by which comparatively easy summer-cut log barking conditions may be handled at reduced water pressure with consequent saving in energy.

The Log Roll Drive Unit

The log-roll motor is connected directly to the trunnion rolls through a speed reducer and chain sprocket drive. The motor must be capable of starting, revolving, and tumbling logs of all shapes and sizes that come within the capacity of the barker. The speed range required of the trunnions will vary from a "creep" for the largest poorly shaped logs to about 40 rpm depending upon the application. About 500 feet per minute of log surface speed is considered to be the upper practical limit of log turning and economical barking speed.

The resistance offered by the bark in its removal varies with the kind and age of the trees, the climatic conditions under which they were grown, the thickness of the bark, the season of the year when cut and the time between cutting and barking. These factors largely determine the upper limit of surface barking speed for a given rate of water flow per inch of nozzle length, within the ranges of effective barking pressure.

A direct current, reversing, variable voltage, adjustable speed shunt motor and gear unit, powered from a reversible polarity adjustable voltage power supply generator usually provides the most satisfactory type of drive for the trunnion rolls for the larger barker units, Figure 12. About ten equal speed steps from near zero to full speed can be obtained economically with this type system and materially assists in obtaining maximum output from the barker. This system permits carrying large currents to produce high torque at low speeds which is often required for the larger sized logs. A per-

Figure 11. Two 800-horsepower, 1,300 pounds per square inch 3,750-rpm pump sets for nozzle water supply

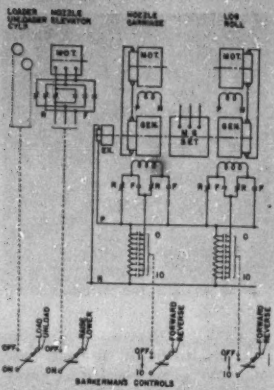


Figure 12. Power and control circuit schematic diagram for d-c variable voltage nozzle carriage and trunnion roll drives with induction motor nozzle hoist drive

fectly shaped 60-inch diameter 20-foot hemlock log from the pond may weigh 17 to 18,000 pounds, representing considerable dead weight and shear friction loading against the trunnions. This loading is very greatly increased when poorly shaped logs having elliptical or odd-shaped butts are encountered as these result in partial displacement of the center of gravity when rotating, and produce locking effects in the trunnions. It is often necessary to start such logs from rest or to reverse-roll them to clear out rot and crevasse.

This requires that the motor be capable of lifting a portion of the log weight in addition to overcoming the friction and shear loading. To provide for these indeterminate loading factors, experience to date indicates that under stalled conditions the motor torque should be capable of exerting a force against the log equal to about 25 per cent of the log weight.

Four-speed constant torque-type squirrel cage induction motors with reversing control have been considered for the larger barker roll drives. To date none of these have been built. However, two small sized barkers are on order which make use of this type drive.

The a-c motor drive is usually somewhat less expensive than the d-c drive and is given preference for this reason. In such cases, it is considered practical to "buck" the largest poorly shaped logs to shorter lengths so they can be more satisfactorily handled and at less overload requirements on the motor.

Large poorly-shaped logs and generally bad timber conditions may require a large percentage of slow-speed high-torque trunnion motor operation. Forced air ventilation of the roll motor may be required for such application. If found necessary this can usually be added without appreciable cost.

Nozzle Carriage Drive Unit

The carriage drive motor is connected to the load through a speed reducer and propulsion cable drum. The cable is attached to both ends of the nozzle carriage over a pulley system which permits driving the carriage in both directions by reeling and unreeling of the drum.

The production output of the barker is directly related to the acceleration, deceleration, and reversible speed characteristics of the carriage drive. Upon being released to travel the carriage should quickly accelerate to the speed selected by the barkerman. This speed should be maintained to the end of the carriage track where equally rapid deceleration causes it to stop. If occasion demands the carriage should be capable of rapidly stopping, starting, and retracking over any portion of the log length to pick up missed bark, clear away rot, or clean difficult sections of the log. In general the carriage should have the feel at the controls of being alive as opposed to that of sluggishness.

For low-production barkers the rates of acceleration and reversibility may not be of great importance. For high production units these become of prime importance in determining the output for a given eight-hour shift. These and other factors can easily determine the number of barking shifts required to match the mill's average chip requirements.

A direct current, reversing, variable-voltage adjustable-speed shunt motor and gear unit powered from a reversible polarity adjustable voltage power supply generator is the correct type of drive for this system. The design should provide for all carriage speeds from a slow creep to the maximum speed as set by the smallest log diameter and nozzle length. The number of equally divided speed steps within these limits may vary with the particular application but in general ten steps will be found adequate.

The maximum speed of the carriage is determined by the maximum speed in revolutions per minute which is considered practical to rotate the smallest log. If excessive speeds of rotation are given to the smaller logs they tend to jump and ride away from the trunnions. Perfect specimens are subject to little of this tendency. For 6-inch diameter logs between 250 to 300 rpm is usually a reasonable upper limit considering the small number of such logs that come in normal production. Assuming no overlapping of the barking jet the maximum speed of the nozzle carriage in feet per minute is:

S = (revolutions per minute of smallest log) (nozzle length in feet)

Figure 13. Maximum inertia and friction horsepower developed for different carriage weights and speeds

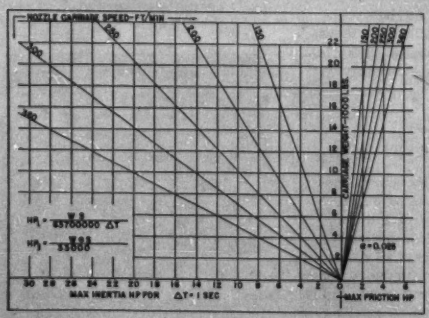


Figure 14. Ratio between maximum horsepower and root-mean-square horsepower for 8,000 pound carriage with 20 feet travel for different accelerating rates

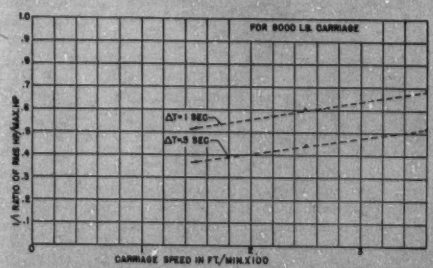


Figure 15. Nozzle carriage, showing nozzle elevating mechanism and cable flexible high-pressure hose, swing pipe, supply pipes, and propulsion cables

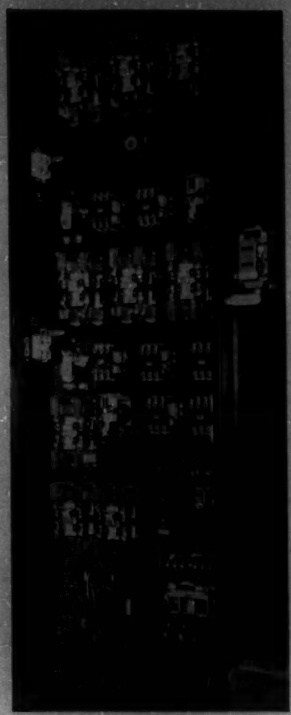


Figure 16. Automatic control cabinet for barker controls, resistors, and protective relays

In the usual application the barker will be designed to handle run-of-the-woods size logs which may for example vary from 6 to 60 inches diameter. The top speed of the carriage should in most cases be based more nearly on the speed of the arithmetical size log or the weighted average size based on wood content. This practical consideration may avoid fictitious calculated requirements for excessive carriage speeds.

For applications where the barker is designed to handle only small size logs higher values of log rotation and carriage speeds may be justified. The factors relating the application to the power characteristics required of the drive are all tangible and lend themselves to easy calculation. The following approach to determine the power requirements and rating of the motor has proved satisfactory.

Assuming uniform carriage acceleration

$$F=\frac{W}{g}a$$

$$a=\frac{S}{60\Delta T}$$

The maximum horse power developed by the motor in overcoming inertia to accelerate the carriage becomes

$$HP_i=\frac{FS}{33{,}000}=\frac{WS^2}{63{,}700{,}000\,\Delta T}$$

The maximum horsepower required to overcome friction is

$$HP_f=\frac{W_e S}{33{,}000}$$

Where

F = force in pounds
S = free running carriage speed in feet per minute
a = acceleration in feet per second per second
W = weight of nozzle carriage complete in pounds
e = coefficient of rolling friction
ΔT = the time in seconds required for acceleration

e is usually based on 50 pounds per ton of carriage weight to give a friction constant of 0.025. The total maximum horsepower required of the motor is then HP_i+HP_f.

This method intentionally assumes that the maximum requirements of the motor will be determined by its ability to commutate the currents established by acceleration as modified by the root mean square horsepower of the duty cycle if this later correction is required.

Figure 13 gives the inertia and friction horsepower for different carriage weights and speeds for an accelerating time ΔT of one second. In figuring the horsepower required for high production barkers the accelerating and decelerating time is usually taken as 0.5 second which doubles the inertia horsepower values given by the chart.

It will usually be desirable to check the root mean square horsepower loading for the particular duty cycle imposed by the carriage movements. The time intervals for the various movements may be established by conventional calculation assuming constant and equal acceleration and deceleration rates. The carriage should be considered to travel a distance equal to the maximum log length plus two times the nozzle length. This allows the nozzle sufficient travel to clear the log on each end. These calculations have been made for an 8,000 pound carriage based on 20 feet total travel for ΔT equal to 0.5 and one second and expressed in 1/1 of the total maximum inertia and friction horsepower for carriage speeds of 150 and 360 feet per minute. The results are shown by Figure 14. These calculations assume no idle time for the carriage. It is important to note that the requirements of the drive may primarily be determined by the peak commutating ability of the motor and its associated generator. Machine rating of 150 per cent should not be exceeded and lesser values of overloading are to be preferred for this highly fluctuating load.

The generator is equipped with killer fields to prevent creep of the carriage with the controls in the "off" position and to assist in regenerative braking.

Nozzle Elevator Unit

It is necessary to adjust the nozzle for proper distance from the log for most effective barking. This may be done by hydraulic or air cylinders, self-locking lead screw, and motor mechanism on the carriage or by traveling cable arrangement to a stationary motor and drum unit apart from the carriage.

The first method presents no particular electrical problems. The others require special consideration as regards application of the motors. Ordinary polyphase

squirrel-cage induction motors have been found satisfactory, either normal torque or hoist type.

In the external cable drum and motor arrangement, Figures 3 and 15, from 8 to 15 operations per minute may be required for the fastest carriage drives having 18- to 24-inch nozzle length. The time interval that the motor runs is very short and little cooling is afforded by rotation to dissipate the heat resulting from the normally high inrush currents. It is considered good practice to overpower the motor to favor the heating problem. In most cases it is necessary to provide for forced air cooling of this motor.

An electric brake is provided on the motor shaft to hold the nozzle positioned when elevation adjustments are not re-required. The reversing controller and brake are interlocked for rapid release so that minimum duty is required of the motor. Figure 3 shows the cable arrangement by which the nozzle position on the moving carriage may be changed by the stationary motor and drum unit.

Barkerman's Control Desk and Cubicle

For maximum production it is essential that the barkerman's controls be designed to minimize both physical and mental fatigue. The variability of log sizes and their conditions greatly multiply the complexity and co-ordinated handling requirements with which he must continually deal. Figures 7 and 12 show one arrangement of control levers and handle operated push buttons by which this was effectively carried out. Arm movement of the levers is required only to make speed or operate selections which is comparatively infrequent. Reversing the carriage, changing nozzle elevation, unloading and loading and reverse rolling are all accomplished by the almost effortless action of depressing corresponding thumb buttons located at natural grip position on top of the control levers. The approach chains, also under the barkerman's direction are foot button operated.

In addition to the operating levers, the barkerman's desk is provided with push buttons and indicating lights for remote starting, stopping, and supervision of the following barkerhouse equipment which logically comes under his direction:

Operate foot button and lockout push-button for control of the fore-deck approach chains.

Main high-pressure water pumps.

Main motor generator set.

Main bark conveyor.

Hydraulic unloader pump motor (when used).

Barking lights.

Carriage backout reset, for use in case carriage overtravels the safety limit switches.

Alarm for wood mill superintendent and millwright.

Screen motors and auxiliary conveyors.

Figure 12 schematically represents the principal electric circuits and control apparatus. The detail control apparatus and devices which set up and perform the necessary functions as initiated by the barkerman are housed in a separate control cabinet, Figure 16.

High-speed nozzle carriage and log roll drives can be controlled economically in both speed and direction by Rototrol excitation for the generators. This arrangement provides a means of limiting the maximum current taken by the motors without regard to the rapidity with which the barkerman may manipulate his controls. A Rototrol controlled system is presently being manufactured in which the barkerman will control both the trunnion and carriage drives by means of a control wheel. The speed of trunnion rotation in either direction will be proportional to right or left hand rotation of the wheel from a central position. Similarly the nozzle carriage speed in each direction of travel will be proportional to the amount the wheel is moved forward or backward from its center or neutral position. The control wheel will be equipped also with finger tip control for raising and lowering the nozzle.

Conclusion

The Bellingham barker represents a distinct advance in round log hydraulic log barking machines. It is simple and versatile as related to the many handling requirements in large-size log debarking. The controls and drives all lend to simple and reliable solution by electrical circuits and apparatus. The success attained to date with this barker indicates that modifications in design and arrangement will be numerous to fit the additional requirements of the very small and very large log debarking.

It is expected also that the barker will find use in other industries and sections of the country as in mining for the barking of poles or mine timbers.

Appendix

Development of formulas for the production output of the Bellingham barker:

Let

R = surface speed of log in feet per minute
L = nozzle length in inches
D = mean diameter of log in inches
N = revolutions per minute of the log
E = barking efficiency
S = free running nozzle speed in feet per minute

Then

$R = \pi DN/12$

$S = NL/12$

$S = \frac{RL}{\pi D}$

Production in cubic feet per hour

$= \frac{RL\ 12}{\pi D\ 12} \times \frac{\pi D^2}{4(12)^2} \times E \times 60$

$= 0.104\ RLDE$

Production (solid cords per hour)

$\cong 0.00104 RLDE$

= Production in cubic feet per hour

$\frac{\pi D^2}{4(12)^2} \times SE \times 60$

$= 0.328\ D^2SE$

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No Discussion

Modern Trends in Hydraulic Turbine Design

WM. J. RHEINGANS
NONMEMBER AIEE

Synopsis: Large welded plate steel spiral casings with plate thickness up to $2^3/_{16}$" for operation under heads up to 435 feet have been manufactured in recent years. On some casings both the shop joints and the field joints were welded without stress relieving by annealing. After three to five years operation there has not been a single case of a weld failure. Other intricate turbine parts such as coverplates, discharge rings, speed rings, Francis runners and impulse wheel buckets have also been fabricated out of welded plate steel. There has been an increase in the use of stainless steel to resist pitting, erosion and corrosion, resulting in reduced maintenance costs. Newly designed self-lubricating bearings and carbon ring seal rings have simplified operation and have eliminated sources of trouble and failure.

THE biggest advancements in the design of hydraulic turbines during the past ten years have been in connection with the materials of construction. There has been an increased use of welded plate steel in place of cast steel due partly to the lower cost of welded structures and the difficulty in obtaining sound castings. There has also been an increase in the use of stainless steel for the various turbine parts which are in contact with high velocity water. Self-lubricating bearings and carbon ring water seals are a distinct improvement in the design of the medium sized units, since they eliminate common sources of trouble.

Welded Plate Steel Construction

As the cost of patterns and steel castings kept increasing relatively faster during the past ten years than the cost of plate steel, it became more and more economical to use a fabricated construction. It has been very difficult also to obtain good, sound, steel castings in the sizes and shapes required for hydraulic turbines. Many of the castings received from the steel foundries have required nearly as much welding to repair cracks and defects as though it were a welded structure to start with. This has not only increased the cost of the castings still further, but has meant critical delays due to either major repairs or the replacement of castings that had to be scrapped.

Therefore when the four 103,000-horsepower 330 foot head turbines were purchased by the United States Bureau of Reclamation for the Shasta power plant in 1939, it was found that it would not only be economical but that it would be possible and practical to construct the spiral casings for these turbines out of welded plate steel rather than cast steel.

Shipping limitations, as to maximum dimensions, indicated that the casings be made up in seven sections. This meant that some type of field joint would have to be used. Several different methods were considered, including welding. However at that time very little information was available regarding the welding of such large structures in the field without stress relieving. Double butt strap riveted joints were therefore used for assembling the sections in the field.

Figure 1 illustrates the casings assembled in the manufacturing shops, and shows the welded shop joints as well as the riveted butt strap field joints. The inlet to the casing is 12 feet 8 inches in diameter, and the plate thicknesses vary from 1 to $2^3/_{16}$ inches. The finished weights of the seven sections, including the cast steel speed ring sections, varied from 26,000 to 58,000 pounds each. Figure 2 shows one of these sections, with the steel plates welded to the cast steel speed ring.

Handling of such large sections in the shops for economical and satisfactory welding procedure presented quite a problem. This was solved by the construction of tiltable welding tables, one of which is shown in Figure 3. This table is 20 feet in diameter and can be rotated through 360 degrees and tilted through an arc of 90 degrees. Figure 4 shows one of the sections of the casing mounted on the welding table for welding and grinding. These tables permitted the use of the Lindy Union melt method (a submerged arc type of welding process) for a large percentage of the joints and made it possible to do all hand welding, chipping and grinding in the position most convenient for the operator. Every welded joint

Figure 1. Shop assembly of welded plate steel spiral casing for 103,000-horsepower 330 foot head turbine for Shasta power plant

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WM. J. RHEINGANS is with the hydraulic department of the Allis-Chalmers Manufacturing Company, Milwaukee, Wis.

Figure 2 (left). One of seven sections of welded plate steel spiral casing for Shasta power plant turbines, showing plate steel welded to cast-steel speed ring

Figure 4 (right). One of seven sections of welded plate steel spiral casing for Shasta power plant turbines on tiltable welding table

was completely X rayed. Defects were chipped out and rewelded.

Because of the riveted field joint construction it was not practical to make a pressure test during erection in the shops. After assembly in the field each casing was tested to 310 pounds per square inch pressure which was double the normal maximum operating pressure. These tests developed no failure or leaks at any point in the casing, and five years of operation have indicated that the welded construction is entirely satisfactory.

The Shipshaw development on the Saguenay River in Canada was the first installation to utilize spiral casings of the all welded construction, where all field joints as well as shop joints were welded. This development consisted of 12 hydraulic turbines rated 100,000 horsepower each at 208 foot head. All of the plates in eight of the spiral casings were welded to each other and to the cast steel speed ring sections. The other four casings all used riveted joints.

Figure 5 shows the partial assembly of one of the welded casings in the shops. The welders are welding some of the plate joints comprising the sections as shipped. Figure 6 shows the assembly of one of the casings in the field before welding of the field joints.

The inlet diameter of each spiral casing is 16 feet, and the plate thickness varies from 7/8 to $1^3/_{16}$ inches. The casing was divided into 12 sections, the size of each section being determined by the maximum size that it was possible to ship. Each section consisted of a number of plates with longitudinal circumferential joints welded together in the shop. None of the plates were welded to the speed ring in the shop, making it possible to construct the speed ring in five sections instead of 11 which otherwise would have been necessary.

No stress relieving or annealing of the welded joints was done either in the shop or in the field. However all welding was done under extensometer and test bar control. The former controlled locked-up stresses and by proper peening, the necessity for stress relieving was eliminated. The test bar control consisted largely of impact tests of sections cut daily from the actual welds of every man. A general requirement of 30 foot pounds (Charpy) was demanded.

Deposited weight of weld rods ranged between one and two pounds per man per hour total elapsed time. The entire casing assembly and welding in the field ranged from two to four weeks and averaged about three.

The units were placed in operation during 1942 and 1943, and after five to six years operation no trouble of any kind has developed on the welded casings.

Another notable all welded spiral casing installation was made for the two 91,500 horsepower turbines for the Fontana development of the Tennessee Valley Authority. This installation is of particular interest because of the welded field joints in combination with a large casing under relatively high head. The gross head on the units varies from 435 feet maximum to 245 feet minimum with 330 feet as the rated head. The spiral casings have a 12 foot diameter inlet with plate thickness varying from $1^1/_8$ to $1^7/_8$ inches.

The speed ring was of cast steel and made up of five equal sections, designed to be rigidly bolted together. The spiral casing was divided into eight sections. Because of the relatively high operating pressures for this casing, it was decided to stress relieve all shop welded joints by annealing. Also, since the highest stress in the casing occurs near the joint between the plates and the speed ring, the sections were so designed that all of these joints were shop welded and therefore annealed. Each section consisted of a number of plates with circumferential joints.

Figure 3 (left). 20 feet diameter tiltable and rotatable welding table

Figure 5 (right). Partial shop assembly of all welded plate steel spiral casing for 100,000-horsepower 208 foot head turbine for Shipshaw Development, Canada

Figure 6. Field assembly of all welded plate steel spiral casing for turbine for Shipshaw Development, Canada

Figure 8. Shop assembly of all welded plate steel turbine parts for 50,000-horsepower, 195 foot head, turbine for Maraetai Plant, New Zealand

Field welding was done on both circumferential and longitudinal joints. All field welding was done under extensometer and test bar control. Each welding crew included a man who did nothing but peen the welds. The amount of peening was controlled by the extensometer readings, thus reducing the locked up stresses.

Tensile test bar specimens were cut daily from the actual welds of every man and tests were made immediately, on a portable tensile testing machine. As a further test of the strength of all the welded joints the casings were tested under 375 pounds pressure which is double the maximum static head on the unit.

Figure 7 shows one of the assembled Fontana casings in the shops. Field joints are clearly visible. The units have been in satisfactory operation since 1945.

In all of the casings described before, there has not been a single case of weld failure or even leakage either during the tests at excess pressures or during operation in the field. Since the welded joints in these spiral casings were all flush, the resulting benefit to smooth flow as compared to a riveted casing was reflected in exceptionally high turbine efficiencies as determined by actual field tests.

The completely satisfactory performance of welded casings under test and in field operation indicate the advisability of giving serious consideration to this type of construction for all future installations. In this connection it should not be overlooked that successful welding can only be obtained through proper design, application and control, based on a broad experience on the part of both the designers and the welders.

In addition to welded spiral casings, recent years have seen a trend towards welded construction of top plates, bottom plates, guide vanes, speed rings and shifting rings. An interesting example of this is the installation of the five 50,000 horsepower, 195 foot head turbines for Public Works of New Zealand, Maraetai plant. All of the principal turbine parts are of welded plate steel construction including the spiral casing, speed ring, top and bottom covers, guide vanes, shifting ring, draft tube liner and pit liner. Figure 8 shows the shop assembly of one of these units and Figure 9 the all welded discharge rings. Figure 10 shows one-half of the all-welded coverplates. The two halves will be rigidly bolted together. This was the most complicated welded structure in the entire turbine. Figure 11 shows the completely fabricated and welded speed ring (in halves) on skids prior to ship-

Figure 7. All welded plate steel spiral casing for 91,500-horsepower 330 foot head turbine for Fontana Power Plant
Casing is on boring mill in shop

Figure 9. All welded plate steel discharge ring for Maraetai turbine, New Zealand

Figure 10. All welded plate steel head cover for Maraetai turbine, New Zealand

Figure 12. One of 18 all welded runners for 30 foot head turbines for Kilbourn Plant.

Runner buckets of die-formed plate steel welded into cast steel crown and plate steel band

Figure 14. Impulse wheel buckets with pressed plate steel bowl, and with lugs and splitter welded to bowl

For 56,000-horsepower 2,200 foot head, impulse turbine at Big Creek 2-A power plant

ment. The pipe braces are used to prevent deformation in transit. The spiral casings will be of all welded construction without stress relieving by annealing. These units have not yet been placed in operation.

Where a sufficient number of duplicate runners are desired so that the die cost per runner is not too great, all welded Francis runners can be built. Up to the present this type of construction has been confined to runners for the lower heads (below 100 feet) because of the varying blade thickness required for the higher heads. Figure 12 shows a Francis runner with die-formed plate steel buckets welded into a cast steel crown and rolled steel discharge band. This is one of 18 replacement runners for the three horizontal sextuplex units of the Wisconsin Power and Light Company, Kilbourn plant, operating under 30 feet head.

This type of construction has several advantages. It produces very smooth runner bucket surfaces; water passages that are true to the desired shape; and uniform discharge openings, all of which tend towards high efficiency and smooth operation.

Fabricated housings for large butterfly valves for use as penstock shut-off valves have also seen increased use in recent years. Figure 13 shows a 15 foot diameter butterfly valve with an all welded plate steel housing, for the Grand River Dam power plant for operation under 115 feet head. The valve wicket is of cast steel. The housing was split to facilitate handling and field erection.

One of the most recent applications of fabricated structures is in connection with impulse wheel buckets. Considerable trouble has been experienced in recent years trying to obtain sound bucket castings. Because of the high water velocities it is imperative that the bucket bowl surfaces be highly polished and absolutely free of pin holes or defects of any kind. This strict requirement results in the rejection of a large percentage of bucket castings and requires considerable repair work and grinding on acceptable castings. By forming the bucket bowls out of plate steel all of these difficulties are eliminated. Figure 14 shows two of 52 fabricated buckets being manufactured for the Southern California Edison Company. The bucket bowls are of pressed plate steel. The splitters are forged steel and are welded to the bucket bowl. The lugs are of plate steel 4 inches thick and are welded to the back of the bucket. These buckets will be installed in the Big Creek 2-A unit rated 56,000 horsepower, 2,200 feet head.

Buckets similarly fabricated were installed in 1944 in a Southern California

Figure 11. All welded plate steel speed ring in halves, for Maraetai turbine, ready for shipment to New Zealand

Figure 13. 15-foot diameter butterfly valve with all welded plate steel housing for Grand River Dam for operation under 115 foot head

Figure 15 (left). Runner for 50,000-horsepower 195 foot head turbine for Maraetai Plant, New Zealand, showing stainless steel strips welded to back sides of runner buckets

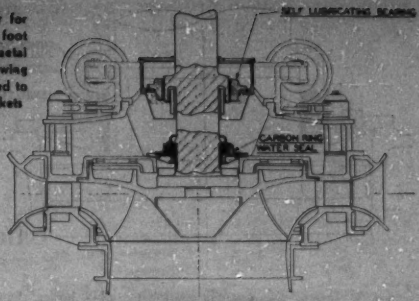

Figure 17. Self-lubricating bearing and carbon ring water seal for Francis turbine

Edison Company, Big Creek plant number 1, unit rated 22,500 horsepower at 1,900 feet head. They have now given four years of satisfactory operation without showing any signs of failure or wear.

Use of Stainless Steel

In addition to the increased use of fabricated plate steel in hydraulic turbine construction, the tendency towards use of stainless steel is increasing very rapidly. Stainless steel has the property not only of resisting the pitting action due to cavitation, but resists corrosive action due to chemicals in the water and resists erosive action of high velocity water which contains solids, such as sand, grit or glacial silt.

The use of stainless steels to resist pitting dates as far back as 1928. This has been mostly in the form of welded stainless steel overlays on mild cast steel surfaces at points where it was anticipated that pitting would occur. A recent innovation has been the use of stainless steel strips welded to the surfaces, see Figure 15.

This latter method not only decreases the cost of surfacing with stainless steel, but insures a more uniform thickness of the overlay and acts as a guide to the true runner bucket shape during grinding and finishing operations.

During the past few years a number of all cast stainless steel runners have been manufactured. Figure 16 shows a 20,000 pound cast stainless steel runner for the Brazos power plant, Texas. The original cast steel runners for this plant wore very rapidly due to excessive corrosion. Repairs with stainless steel on these runners indicated that stainless steel would have a high resistance to the corrosive action. Since the corrosive action was not confined to small areas but affected the entire runner, the use of a solid stainless steel casting was justified.

Solid stainless steel runners have also been successfully used where other materials were subject to considerable erosive action due to sand or grit in the water.

Use of stainless steel on hydraulic turbine parts have proved to be beneficial when used in connection with guide vanes, facing plates above and below the guide vanes, wearing rings both stationary and on the runner, discharge rings, and shaft sleeves for the main shaft where it passes through a stuffing box.

Stainless steel needles for impulse wheels have given years of very satisfactory service in many instances where mild steel or bronze needles eroded after a short period of operation. The use of stainless steel for valve seats for butterfly valves, pressure regulators and similar applications has been very successful in eliminating wear due to leakage.

Francis turbines for heads above 700 feet are subject to considerable wear due to both cavitation and erosion, aggravated by high velocity of flow. Some of the recent high head installations are therefore using stainless steel for all turbine parts which come in contact with high velocities. This includes the guide vanes, facing plates, runners, wearing rings and discharge ring. It is believed that this will largely eliminate or reduce the wear generally encountered with high head installations. If operating experience shows that stainless steel meets the expectations of the designers, reaction turbines can then be used for heads which had previously been considered impractical.

Figure 16 (left). 20,000 pound cast stainless steel runner for 17,000-horsepower, 115 foot head turbine for Brazos River power plant

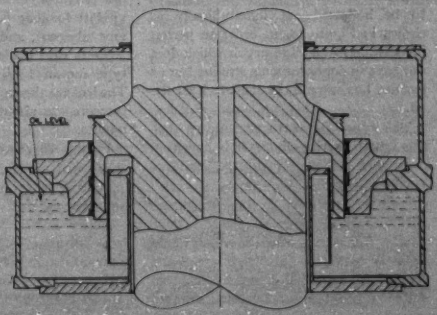

Figure 18. Details of self-lubricating bearing used on hydraulic turbines

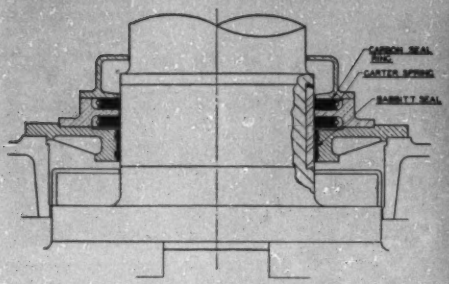

Figure 19. Details of carbon ring water seal used on hydraulic turbines

Figure 20. Test of 18½ inch diameter carbon ring water seal showing leakage under 20 pounds pressure

Self-Lubricating Bearings and Carbon Ring Seal Rings

Other trends in the design of hydraulic turbines are noted in improved bearing designs and seal rings. Figure 17 shows the location of these new designs on a vertical shaft Francis turbine.

With reference to the main bearing, hydraulic turbine designers have borrowed the self-lubricating bearing with appropriate modifications which has been used successfully on hydrogenerators for many years. Figure 18 shows an outline of this type of bearing. The bearing runs in an oil bath covering about one-third of its vertical length. The babbitt in the bearing has longitudinal grooves so slanted that the rotation of the shaft produces a pumping action. Oil is thus forced from the oil reservoir to the top of the bearing where it floods the entire upper portion of the bearing. In addition the grooves are so shaped that the shaft will pick up oil from them and carry it around to the remainder of the bearing surfaces.

The main feature of this bearing which makes its use possible is a leak proof oil reservoir. This means that the shaft has to be hollowed out longitudinally as shown in Figure 18, in order to permit installation of an oil reservoir which does not have to depend upon a stuffing box or packing box around the shaft to hold the oil.

This type of construction means that the actual bearing diameter is considerably larger than the rest of the shaft diameter, and therefore the use of the self-lubricating bearing has been confined to shafts of 20 inches or less diameter.

However the simplicity of the lubrication, the elimination of all mechanical means for circulating the oil and the economical type of construction have caused it to be very much in demand for the smaller size units. In addition to the large number of self-lubricating bearings of this type in operation on hydrogenerators, one has been in operation on the 12,500-horsepower 230 foot head turbine for Puente Grand power plant, Mexico for five years, and so far has given entirely satisfactory service.

The type of stuffing box, or packing box commonly used where the shaft passes through the top cover, has not been too satisfactory. It requires constant adjustment, because if it is too tight, it heats up and scores the shaft, and if it is too loose, the leakage becomes too great. It produces sufficient wear on the shaft so that renewable sleeves usually of stainless steel are installed at this point.

Figure 19 shows a carbon seal ring which can replace the stuffing box. This seal consists of two split carbon rings held against the shaft by means of garter springs. The two rings are separated by a spacer. Water and grease lubrication are provided, although the rings should operate for long periods of time without any lubricant, and without heating.

Tests were made on a seal ring of this type for an 18½ inch diameter shaft. The leakage through the seal at 20 pounds pressure was about a quart of water per minute.

Figure 20 shows the test setup.

The advantages of the carbon seal ring are that it requires no adjustment or attention, does not require renewal, and should produce very little wear on the shaft. Therefore it can be used in places which would ordinarily be inaccessible or which would require special design and construction to make the packing ring easily accessible for adjustment. There is a good possibility that the carbon seal ring will cause so little wear on the shaft that the usual stainless steel sleeve can be eliminated. Seal rings using lignum vitae blocks instead of carbon, have been in operation for over ten years and have been entirely satisfactory. Carbon seal rings have not as yet been in service on hydraulic turbines, but based on their performance on steam turbines under high temperatures and pressures, and based on laboratory tests, it is predicted that they will be entirely satisfactory.

Conclusion

In conclusion it can be stated that the general trend in turbine design is towards the better and more economical use of available materials as characterized by the increased use of welded plate steel structures. The increased use of stainless steel and the new types of bearings and seal rings indicate the desire of the designers to reduce field maintenance as much as possible, with a resulting reduction of costly outages.

No Discussion

Telemetering Circuits—Metallic and Carrier

M. A. BOSTWICK
MEMBER AIEE

TELEMETERING is an art that has enjoyed a steady development for 30 years. In its beginning it consisted simply of measuring an electrical quantity at one point and transmitting it over metallic circuits to a remote point for indication. It saved many steps and telephone calls by placing continuous indications of magnitude of current, voltage, and power in front of the power station operating engineer. From this simple beginning it has expanded to encompass entire power systems and interconnections to adjoining power systems. The data that are gathered in this manner are so accurate and the methods that are used to totalize that information are so reliable that the telemetering equipment and the associated automatic load control apparatus are now performing many functions that directly aid the power system dispatcher himself. The value of such telemetering systems and automatic load control have been amply demonstrated in many installations.[1–4] Recent developments in telemetering systems have been described also in several papers.[5–8]

Typical telemetering systems are illustrated by block diagrams, Figures 1 and 2. Figure 1 illustrates the measurement, transmission, and recording of power and reactive voltamperes throughout a power system. The information is placed in front of the load dispatcher to guide him in controlling operation of the power system. Figure 2 illustrates an expansion of the system to include automatic load control and thus relieve the dispatcher of routine work. This automatic control provides increased operating efficiency as described in references.[1–4]

This paper is written to provide a review of the art of telemetering, with special emphasis on the circuits that are used to transmit the information from the points of measurement to the points of utilization. The paper includes brief descriptions of the general types of telemetering apparatus that are now in use; a description of metallic, carrier frequency and radio frequency circuits that are used for transmission of the information, and a look into the desirable future. The paper also indicates limitations of the various circuits, and provides a guide for future planning of the economic use of telemetering equipment.

To provide a clear picture of the telemetering problem, it is desirable to classify the many different types of telemetering systems that are available. All telemetering apparatus that is now on the market can be divided into the following general types: voltage; current; and impulse.

The different circuits that are used to transmit the metered quantities can be classified as metallic, carrier frequency, and radio frequency.

It is proposed to discuss the various types of telemetering briefly so as to obtain a common ground for discussion. The various telemetering circuits will then be described. This description will include a discussion of circuit requirements and limitations. Finally the various types of telemetering apparatus and circuits will be discussed in combination with carrier relay and communication circuits to provide dependable and economic solutions to the telemetering problem.

General Types of Telemetering Systems

Voltage Types

Telemetering transmitters which employ voltage magnitude as a means to transmit intelligence include; thermal converters, slide wire position indicators, and polyphase wound rotor motors. The thermal converter employs a polyphase ($E+I$ and $E-I$) heating element to energize thermocouples so as to produce millivolts proportional to watts or reactive-volt amperes. Similar thermal converters are made to provide millivolts proportional to voltage squared or current squared. Limited amounts of energy can be drawn from these devices to actuate indicating or recording instruments. Potentiometer-type recorders which draw no energy from the measuring device are usually used. A metallic circuit that is well insulated and free from disturbing voltages must link the thermal converter with its associated recorder.

A retransmitting slide wire is frequently built into the recorder to provide a means of totalizing recorded quantities. This slide wire may be operated from alternating current when totalization is performed with instruments on the same panel.

Retransmission of the recorded quantity over an appreciable distance requires use of a d-c slide wire, or the use of impulse telemetering. Slide-wire potentiometers can be used in a similar manner to transmit appreciable amounts of energy to a remote point to actuate indicating instruments directly. This is done to indicate the mechanical position of some device, as a water level indicator, a tap changing regulator and so forth.

Servomechanisms are used in a similar manner to indicate position at a remote point. Motor operated devices of this kind require a 3-wire circuit and voltage references that are in phase at each terminal. Large boiler room indicators that are operated from a recorder in the

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M. A. Bostwick is with the Westinghouse Electric Corporation, Newark, N. J.

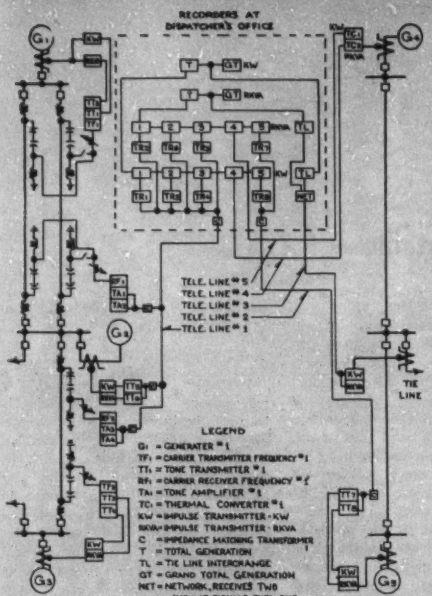

Figure 1. Block diagram of telemetering circuits using metallic and carrier circuits

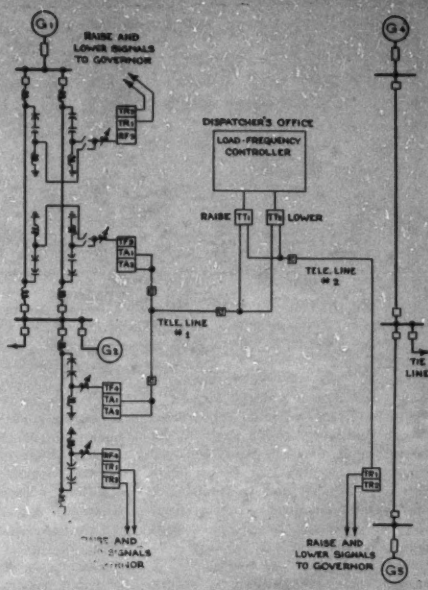

Figure 2. Circuit additions to Figure 1 for load control

same power plant are an example of this type of telemetering.

Current Types

Current operated telemetering devices generally differ from the voltage operated devices in the magnitude of energy that is used to transmit the intelligence. The voltage operated devices, more especially the thermal converter and retransmitting slide wire, operate with millivolts. Practically no energy is transmitted. The current operated device controls the transmission of considerable amounts of energy which can actuate the receiving indicator or recorder directly. Special means are used to control the transmission of current without affecting the accuracy of the primary measuring element. These means include the current balance transmitter which balances the magnitude of current that is sent to a remote indicator against the torque of the primary measuring element. This balance is usually obtained indirectly through the aid of sensitive platform balances, or by the use of photo cell devices that will not affect the accuracy of the primary measuring element. The transmitter voltage that is used in these circuits is in the order of 50 to 100 volts. This makes the current balance transmitter less sensitive to voltage disturbances or leakage that may be encountered in relatively long metallic circuits.

Impulse Types

Impulse telemetering as described in this paper includes: transmitters that operate at a rate that is proportional to the metered quantity, transmitters that operate at a fixed rate with an impulse duration that is proportional to the metered quantity, and frequency transmitters which are for the purpose of this discussion impulse rate transmitters.

The problems that are encountered in transmitting signals for these systems are all characterized by the impulse rate and impulse duration systems. Since these telemetering systems employ a rate or duration of signal to transmit intelligence, practically independent of the magnitude of the transmitted signal, they are well suited to control of carrier or space radio circuits. They are similarly relatively free from errors that might be introduced by extraneous voltages in metallic circuits.

Interconnecting Circuits

Metallic Circuits

The maximum length of metallic circuits that can be used between the transmitters and recorders that have been described is usually determined by economic factors. Thermal converters, retransmitting slide wires, or a number of these devices connected in series for totalization are usually operated over metallic circuits of less than five miles in length. However, installations are now in service, operating over typical telephone circuits where the distance between transmitter and receiver is as great as 25 to 30 miles. Disturbing voltages, that appear between wires, or leakage in the form of direct current passed through wet cables or across dirty terminals, cannot be tolerated in circuits of this kind. Twisted pairs of wires, in cable maintained in accordance with telephone practice for full metallic ungrounded circuits, are required.

Current operated transmitters are capable of being operated over circuits of greater length. However, the current type finds its principal application in circuits that are not equal to telephone quality (in this paper "telephone quality"

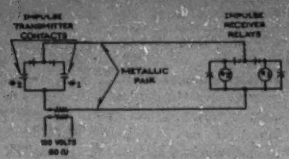

Figure 3. Network circuit for telemetering two quantities over one metallic pair

requires that leakage resistance between wires, or from wire to ground, shall exceed 50,000 ohms). The relatively high level of voltage that is used in current operated telemetering circuits permits greater voltage disturbance in the metallic circuit. Automatic telemetering controls that hold the circulating current to a correct magnitude also tend to limit the effect of disturbing voltages. Current operated transmitters can also be used to operate several receivers connected in series.

Impulse telemetering is generally less sensitive to the introduction of extraneous voltages in the metallic circuit than either the current or voltage types of telemetering. The interfering voltage between wires must be large enough to operate the impulse receiver to affect the accuracy of telemetering. Momentary pulses of disturbing voltage will have practically no effect on the received signal as the indication or record is proportional to the rate or duration of the received impulse. Impulse telemetering also has the advantage of being able to utilize phantom, simplexed, and network circuits. Either direct or alternating current can be used. These circuits permit the simultaneous transmission of two or more signals over a given pair of wires. An example of the network circuit is shown in Figure 3.

The foregoing considerations are summarized in Table I.

Protection of Metallic Telemetering Circuits

Metallic telemetering circuits should be studied to make certain that adequate protective devices are installed. This is required to avoid injury to operating personnel and damage to equipment. Leased telephone circuits usually include the necessary protective features. However, the protection is primarily designed for telephone circuits and may not be adequate to prevent damage to sensitive terminal apparatus. This is particularly true when disturbing voltages are of a magnitude that will operate only one protector block so as to apply an extraneous potential between wires. Generally the protection will consist of 350-volt carbon blocks and 0.35 ampere fuses. These will be supplemented by neutralizing transformers, if disturbing voltages in excess of 350 volts are likely to be impressed on the leased circuit.

The general problem of protecting metallic circuits has been previously discussed.[9,10] The first of these papers describes the manner in which disturbing voltages are impressed on the metallic circuit and illustrates use of a two winding neutralizing transformer. The second paper[10] describes use of a mutual drainage reactor for protection of low-voltage metallic circuits. The neutralizing transformer is particularly well adapted to the protection of open pilot circuits that have a relatively high level of insulation. It is rated at 4,000 volts, and operates to distribute the voltage stress in the form of IZ drops across the windings of neutralizing transformer itself. The mutual drainage reactor is particularly valuable for the protection of low-voltage a-c circuits. It operates to distribute the voltage stress by IZ drops in the metallic circuit itself. The reactor has sufficient impedance (between wires) to avoid interruption of many a-c circuits, even when it is actively "draining" extraneous voltages from the interconnecting circuit.

Audio Frequency Tone Channels on Metallic Circuits

As many as ten audio frequency tone transmitters and receivers are available for simultaneous transmission of telemetering signals over a pair of wires. Impulse telemetering must of course be used. This installation of carrier and audio frequency tone transmitting equipment is illustrated in Figure 1. As shown, it includes practically all types of carrier and metallic telemetering circuits. Installations of this kind must of course be co-ordinated with telephone circuit requirements.

Carrier Circuits for Telemetering

Carrier telemetering circuits, as discussed in this paper, refer primarily to power line carrier channels. They include:

1. Single frequency telemetering circuits.
2. Combined relay and telemetering circuits.
3. Audio frequency tone modulated telemetering circuits.
4. Amplitude modulated carrier circuits for combined communication, telemetering and protective relay control.
5. Single side-band telemetering circuits.
6. Combined single side-band telemetering and communication circuits.
7. Single side-band circuits for communication, telemetering and protective relay control.

In the following discussion reference will be made to typical amplitude modulated and single side band carrier equipment. All components of this equipment are assembled on standard 19 inch panels for easy inspection. New units can be readily added as service requires new features. The units include all elements that are required to assemble many different combinations of equipment. These units include: transmitters, double receivers for relay control and communication, superheterodyne receivers, audio frequency tone transmitters, tone receivers, hybrid units for 2-wire telephone extensions, code bell calling units, selective ringing units,

Table I. Summary of Metallic Circuit Requirements

Type of Telemetering Transmitter	Magnitude of Signal Impressed on Metallic Circuit: Voltage	Magnitude of Signal Impressed on Metallic Circuit: Current	Type of Signal That Carries Intelligence	Metallic Circuit Required	Number of Signals per Pair of Wires
1. Thermal converter	50 millivolts	0	Voltage magnitude	Highly insulated twisted pair with no appreciable leakage	1
2. Slide-wire retransmitter	1 volt	0	Voltage magnitude	Highly insulated twisted pair with no appreciable leakage	1
3. Slide-wire position indicator	125 volts	As required	Voltage magnitude	Dependent upon accuracy required	
4. *Servo-mechanisms	75 volts	0-550 milliamperes	Voltage magnitude	3-wire 100 ohms per wire	1
5. Current balance transmitter	125 volts	Milliamperes as required	Current magnitude	Substantial pair not necessarily twisted	1
6. Impulse telemetering	125 volts	Milliamperes as required	Impulse rate. Impulse duration	Substantial pair not necessarily twisted	—2 (See Figure 3)

* "Synchro-Tie."

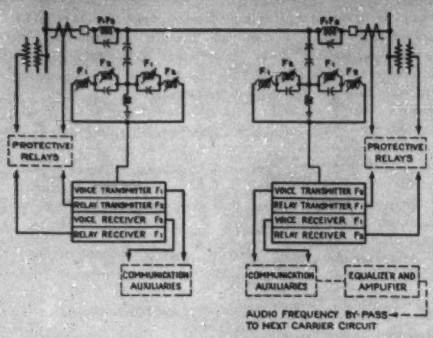

Figure 4 (above). Multipurpose carrier for communication, telemetering, and protective relay

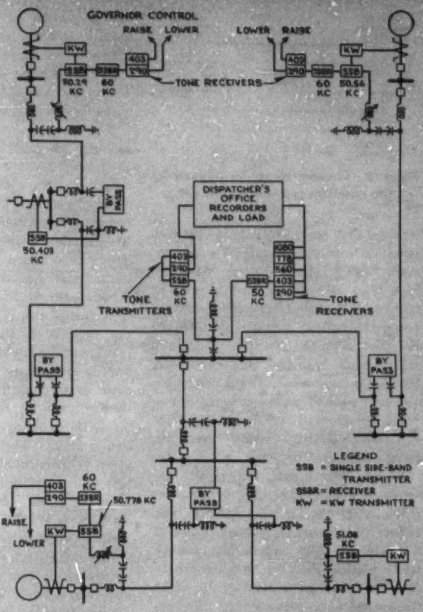

Figure 5. Single side band carrier telemetering and load control circuits

single side band auxiliaries, d-c controls and rectifiers for a-c operation.[11]

1. Single frequency telemetering circuits require one transmitter and one receiver for each quantity transmitted. The carrier equipment can be of the standard relay design which requires a channel width of approximately 2 kc (when sharply tuned) or of the crystal controlled narrow-band type that requires only a few hundred cycles of the carrier frequency spectrum. The relay type of transmitter has the advantage of being standard equipment for use in protective relay control circuits. It can be modified also by the addition of tone transmitters to make it suitable for transmission of as many as ten telemetering signals simultaneously over one carrier channel. It is readily adjusted to operate at any frequency in the chosen frequency band.

2. One quantity can be telemetered over a protective relay carrier circuit, simply by keying the carrier transmitter and adding auxiliary equipment to prevent ringing carrier alarms by the telemetering impulses. Time delay features are usually added to control ringing from either line terminal for emergency communication. Protective relay action supersedes all other functions for the duration of transmission line faults. Telemetering must be interrupted when the circuit is used for communication. Circuits of this kind are used primarily for branch circuits, to transmit a single measurement into a trunk line. The impulse signal is repeated by auxiliary relay action. The relay receiver that is used in telemetering circuits of this kind is capable of operating through a carrier frequency loss of 30 to 33 decibels.

3. The single frequency telemetering circuit described in the foregoing paragraph 1, can be expanded by the addition of audio frequency tone transmitters and receivers to make it suitable for trunk line operation. As many as ten audio frequency tones can be keyed simultaneously to transmit ten quantities to a remote point. With such a circuit a channel width of approximately 6 kc is required. The signals can be received on the tuned-radio-frequency receiver, (the broad band receiver of a relay set) which is capable of being operated through 50–53 decibels. A superheterodyne receiver which is available also for use on telemetering and communication circuits, is capable of operating through 80 decibels. Tone transmitters and receivers must, of course, be added for each telemetered signal.

4. Protective relay circuits are readily expanded to provide continuous telemetering of four quantities with simultaneous point-to-point communication. High pass filters are provided to effectively separate telemetering and communication circuits. Double frequency carrier operation is required to permit conversation in each direction without interrupting the telemetering channel. All operation must be superseded by relay action momentarily during power system faults.

To assure high speed clearing of faults, when the line is reenergized from the telemetering receiving terminal, a circuit is required to interrupt the telemetering transmitter. This function can be performed in one of two ways.

A. Voltage fault detectors connected to line potential transformers can be used to stop carrier on loss of voltage.

B. A double frequency circuit can be used. For example as shown in Figure 4, frequency 1 is used for telemetering and communication from terminal *A* to terminal *B*. Frequency 2 is used in the reverse direction. Each relay-type receiver includes two units, a narrow band saturated single tube receiver for relay control and a broad band TRF receiver with automatic volume control for reception of voice and telemetering signals. The voice receivers are set to receive telemetering frequencies as described before. The relay receivers are set to receive the "inverse" frequency. The protective relay control circuits are adjusted to initiate transmission of frequency 2 from terminal *A* and frequency 1 from terminal *B*. High-speed relay action is thus assured under all operating conditions. The relay receivers cannot be blocked by reception of a telemetered signal as they operate on different frequencies. The essential feature is, that a carrier blocking signal cannot be transmitted by the communication or telemetering circuit. Consequently high-speed carrier relay protection is assured

even at a time when fault current is not transmitted through one line terminal in sufficient magnitude to operate protective relays.

Since relay controls are independent of communication and telemetering circuits, automatic dialing and all forms of communication auxiliaries can be used. Extensions of the communication or telemetering circuit beyond the line terminals must be made by audio frequency by-pass circuits. When two or three audio frequency by-passes are used for communication circuits, the audio frequency response is maintained essentially flat from 100 to 3,000 cycles by addition of equalizing amplifiers. If only a few audio frequency tones are to be transmitted on through adjoining carrier channels, audio frequency tone amplifiers can be installed to receive, filter, and amplify each tone. The output of the tone amplifier can then be used to modulate the next carrier transmitter.

In this manner tones can be repeated through a number of line sections. As previously stated one audio frequency by-pass can be made around a line terminal without use of auxiliary equipment. Each additional by-pass circuit requires tone amplifiers for telemetering tones, or an equalizer and amplifier to repeat the entire audio spectrum.

Directional comparison carrier relay controls can be operated through the higher frequency tone channels if desired. Circuits of this kind have the advantage of permitting telemetering circuits to be operated continuously, without interruption for protective relay control.

5. Single side-band carrier channels are highly selective and are particularly well suited to transmission over long distances. As pointed out in previous papers,[12,13] all of the carrier energy is available for transmission of the desired signal. The single side band carrier circuit uses extremely accurate frequency controls. It inherently provides a carrier receiver that is capable of receiving ten signals simultaneously, from widely separated points in the power system using only one carrier frequency. These signals may include a number of tone modulated signals that are transmitted from a given point, and single quantities transmitted from different points. A circuit of this kind is illustrated in Figure 5. As indicated in the block diagram, audio frequency tone transmitters are required only at locations where more than one quantity is to be transmitted. The single side band transmitter itself, can be tuned to transmit the equivalent of one tone modulated single side-band signal, either 100.15, 100.209, 100.403, 100.29, 100.56, 100.778, 101.08, 101.5, 102.08, or 102.9 kc for example. These tone modulated signals can be received as separate and distinct tones by tone receivers operating with one single side-band receiver.

Load control signals can be broadcast from the dispatcher's office by a second carrier frequency. In this manner it may be possible to establish an entire telemetering system with two carrier frequencies.

6. The features described in paragraph 5 can be obtained in combination with two frequency communication circuits. However, in this case, the number of quantities that can be telemetered is limited to five in each direction using 150, 209, 290, 403, and 2,900 cycle tones separated from the voice circuit by suitable filters. This leaves a frequency band from 500 to 2,500 cycles, which is adequate for clear, intelligible speech. If the 2,900-cycle tone is not required for relay or telemetering control, the speech channel will include frequencies from 500 to 3,000 cycles per second.

7. The single side band carrier equipment can be used in a similar manner for communication, telemetering, and protective relay control. As previously indicated, the protective relay circuit will employ the 2,900-cycle tone channel, speech and telemetering circuits will use frequencies mentioned before.

A summary of these considerations is given in Table II.

Radio Frequency

The use of microwave radio for transmission of telemetering, communication, and protective relay control signals presents many interesting possibilities. Frequencies in the order of 952 to 960 megacycles are being used experimentally for this purpose. Investigations indicate that space radio can provide reliable circuits for multiple uses. The signals are essentially transmitted through a line of sight. A narrow beam of microwave energy approximately 20 degrees wide

Table II. Summary of Available Carrier Frequency Telemetering Circuits

Type of Carrier Equipment	Number of Telemetering Circuits	Number of Carrier Frequencies Required	Auxiliary Functions: Communication	Auxiliary Functions: Protective Relaying	Auxiliary Functions: Supervisory Control	Auxiliary Functions: Load Control	Permissible Carrier Attenuation
Single transmitter and receiver (relay type)	1	1	*	*	*	*	30-33 db+
Crystal controlled	1	1	–	–	–	–	80 db+
Relay transmitter and receiver	1	1	1	1	1	*	30-33 db
Single transmitter with superheterodyne receiver	1	1	*	*	*	*	80 db
Tone modulated transmitter with relay receiver and high pass filters	4	2	1	1	*	*	30-33 db relay channel 50-53 db audio channel
Tone modulated transmitter with superheterodyne receiver	10	1	*	*	*	*	80 db
Tone modulated transmitter with superheterodyne receiver, high and low pass filters	1	2	1	1	1	2	80 db
Single side band telemetering transmitter and superheterodyne receiver with tone filter	1	1	*	*	*	*	90+ db
Single side band tone modulated telemetering transmitter with superheterodyne *SSB* receiver	10	1	–	–	–	–	90+ db
Single side band tone modulated carrier telemetering, relaying and communication with superheterodyne *SSB* receiver	4	2	1	1	*	*	90+ db

* Indicates functions that can be added.

will be transmitted. These considerations, and local obstructions will fix the height of towers and distances between towers. Two antennas are required at each terminal for double frequency operation. Each radio channel should be capable of transmitting a large number of signals simultaneously, in addition to providing communication circuits. Consequently it is to be expected that equipment of this kind will be suitable for the transmission of many signals between locations that cannot be economically served by other means.

Circuit Combinations

As indicated in Figures 1 and 2 it is expected that different types of telemetering measuring devices and transmitters will be used in assembling power system measurements at the power system dispatcher's office. Single measurements, that are made within Metropolitan areas, can be most easily made and transmitted to the dispatcher's office by use of thermal converters, telephone cable circuits, and potentiometer recorders.

At greater distances from the dispatcher's office measurements of a number of circuits at one location can be most readily totalized by use of multielement watt meters. These meters are arranged with photoelectric controls to transmit impulses proportional to the measured quantity. The impulses can be transmitted either by metallic circuits or via carrier circuits to the dispatcher's office. Impulses can be repeated by addition of auxiliary relays at pilot wire terminals to control carrier transmitters. Polyphase measurements can also be totalized by use of a number of thermal converters at one location. The sum of these voltages can be used to control a potentiometer recorder with impulse duration transmitters, which in turn control impulse transmission to the dispatcher's office.

In some power systems, it will be found most economical to use protective relay and communication circuits for the transmission of impulses to control locations. At these points the impulses can be repeated by audio frequency bypass circuits or mechanical means to control multitone transmitters on main line circuits. In other systems the use of narrow band transmitters, sending impulses direct to multi-circuit receivers as shown in Figure 5 will be preferred. Each application must be studied as an individual problem to determine the best solution.

Totalization

Information that is transmitted to the dispatcher's office, in the form of impulses, is usually recorded as measured to indicate power flow throughout the system. These quantities are then totalized through action of slide-wire attachments on the individual recorders. Alternating-current slide wires are used for totalization when all instruments are located on one switchboard. Direct-current slide wires are used when the recorders are more widely separated. The speed of totalization is dependent upon operation of mechanical recording devices. Because of this, it is desirable to totalize quantities electrically wherever such totalization can be obtained without omitting desirable records.

Impulse receivers can be connected in series to provide this totalization. Indicating instruments can be operated from each impulse receiver that is used in a totalizing circuit. However, the totalization must be performed through retransmitting slide wires when potentiometer recorders are controlled from impulse receivers. This is required to avoid interaction between recorder and impulse receiver circuits. These considerations indicate desirable features for future development. Totalization should be performed electrically wherever possible to avoid needless delay in mechanical circuits. It is essential also to perform that totalization in a manner that will not introduce undesirable time delay in the electrical circuits.

Recent developments in transmitters[6,7,8] have substituted electronic devices for mechanical features to obtain increased accuracy and speed of telemetering. Further developments in totalizing means are to be expected.

Conclusion

The art of telemetering is gaining in importance as its use is extended in the field of power system control. Increased numbers of power system interconnections have added to the number and complexity of system dispatcher's duties. The additional sources of information and automatic controls that can be gained by telemetering not only aid the dispatcher, but enable him to obtain increased operating efficiency of the entire power system. These desirable ends can be gained most easily by studying the various types of telemetering equipment and the transmitting circuits that are available and using equipments that are best suited to the application.

The crowding of carrier channels and the desire to obtain maximum use of equipment dictate increased consideration of circuit combinations that have been discussed in this paper. The single side-band carrier circuits are of particular interest since they offer the double features of carrier frequency conservation and added sensitivity.

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5. A Telemeter Transmitted Without Moving Contacts, **Carl Oman.** *Westinghouse Engineer* (East Pittsburgh, Pa.), May 1948.

6. Electronic Telemetering System, **G. E. Foster, W. M. Kiefer.** AIEE Transactions, volume 66, 1947, pages 802–7.

7. A Faster Telemeter for Carrier Current Channels, **E. E. Lynch, H. C. Thomas, G. S. Lunge.** AIEE Transactions, volume 67, 1948, pages 288–94.

8. New Devices Derived from a Torque Balance Telemeter, **W. H. Burnham.** AIEE Transactions, volume 67, 1948, pages 307–10.

9. Protection of Pilot Wire Circuits, **E. L. Harder, M. A. Bostwick.** AIEE Transactions, volume 61, September, 1942, pages 645–51.

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12. A New Single Side-Band Carrier System for Power Lines, **B. E. Leuenan.** AIEE Transactions, volume 66, 1947, pages 826–30.

13. Power Line Carrier Modulation Systems, **R. C. Cheek.** *Westinghouse Engineer* (East Pittsburgh, Pa.), March 1945.

Discussion

H. C. Thomas and **E. E. Lynch** (General Electric Company, West Lynn, Mass.): The classification of telemetering systems given by the author is abbreviated as compared with the accepted AIEE standard definitions of types, and the reasons for his deviation are not apparent. In fact, since the paper is written with special emphasis on the circuits that are used to transmit the information from the points of measurement to the points of utilization, it would appear important to classify the telemetering systems from this point of view.

The most serious consequence of the abbreviated classification which the author uses is in connection with impulse telemeters. In this classification is included telemetering systems which employ, as translating means, impulse-rate, impulse-duration, and frequency. Each of these translating means may call for transmission

circuits having different characteristics.

In the case of impulse-duration systems, it is usually important that the wave-shape of the transmitted signal be faithfully preserved. Thus, it is important to select transmission circuits having a relative wide band frequency response. This requirement is often not present in impulse rate systems, where the wave-shape of the signal at the receiver end of the telemeter channel may be of less importance.

Frequency-type telemetering systems are distinguished from impulse-rate systems in that the electric signal used for transmission is continuous, rather than intermittent, that is, usually a sine wave. This is an important difference from the point of view of the transmission circuit. The continuous signal can be designed to have a minimum harmonic content, whereas the intermittent signal is rich in harmonics. The presence of harmonics in the transmitted signal subjects the telemetering system to the possibility at least of serious interference. This is especially true in carrier-current transmission systems wherein several telemeter signals are superimposed on a single carrier signal, using audio tones and double modulation. Thus, in frequency-type telemetering systems, selectivity requirements of the transmission channel are minimized.

G. M. Thynell (The Bristol Company, Waterbury, Conn.): The valuable data contained in Bostwick's paper would have been more informative if he had recognized the standard classification of telemetering systems given in the "Definitions of Electrical Terms"[1] and adopted in the AIEE reports on the subject. The author's statement to the effect that, "All telemetering apparatus that is now on the market can be divided into the following general types: voltage, current, and impulse," according to the above mentioned standards is incorrect. Frequency-type systems have been completely ignored, and position-type systems have been confused with voltage-type systems, as indicated by the inclusion of servo-mechanisms under the heading of Voltage Types.

Telemetering systems of the frequency type are commercially available in at least four distinct forms; and those of the position type from at least eight different sources. Among the latter may be mentioned the author's own company which offers the "Synchro-tie" as in the position class of telemetering. The author, on the other hand, has classified this system as of the voltage type. While the several types of carrier equipment listed and discussed under the heading, Carrier Circuits for Telemetering, include only those manufactured by the author's company, no such qualifying statement appears in the text; and without such a limitation the classification is manifestly incomplete. A similar comment may be directed to such statements as "All components of this equipment are assembled on standard 19-inch panels. . . . . ". While this is doubtless true for the apparatus which the author had in mind, it does not necessarily hold for corresponding equipment obtained from other sources.

REFERENCE

1. AMERICAN STANDARD DEFINITIONS OF ELECTRICAL TERMS. AIEE, New York, N. Y., 1942, 311 pages.

G. S. Lunge (General Electric Company, Schenectady, N. Y.): Under the head of Totalization, the author states that "recent developments in transmitters have substituted electronic devices for mechanical features to obtain increased accuracy and speed of telemetering." This statement refers only to transmitter changes, whereas two of the systems employed improved receivers.

Without an improved receiver, it is difficult to understand how either the accuracy or the speed of response of the telemeter system could be increased as compared with earlier designs, bearing in mind that the very high damping required in the receiver was the principal source of slow response. The author's statement was presumably intended to apply to reference 5.* The other two references (6 and 7) describe telemeter systems that include electronic receivers that are free from moving parts and hence permit the use of sufficiently high signal (keying or modulating) frequencies to make it possible to dispense with high damping in the receiver circuits or in the end device (indicating or recording instrument). Only the system mentioned in reference 7 takes full advantage of this possibility; the over-all response time of the system described in reference 6 is stated as 20 seconds, including the time of response of the end device (recorder).

* See author's references.

E. W. Kenefake (General Electric Company, Syracuse, N. Y.): Telemetering circuits are of general interest to the power industry due to the increased use of telemetering in the normal operation of a power system and this paper is a timely presentation. However, it would have been worth-while to have amplified certain data and application information presented in this paper.

In discussing the operating range of various types of equipment, the author points out that the operating range, depending upon the receiver used, may be 30–33, 50–53, or 80 decibels. Also, in Table II, the author mentions a rating of 90+ decibels for the single side band equipment. Is it proper to assume that these ranges are laboratory operating ranges, indicating the ultimate capabilities of the equipment, when no line noise exists? This question arises, because in a practical application on a power line involving line noise, the available operating range of a given type of channel cannot be extended merely by the use of a more sensitive receiver. To preserve the same signal-to-noise ratio this must be accomplished by increased transmitter power or by narrowing the band width. If the receiver selectivity is already as narrow as the channel will permit, then only transmitter power can be increased to improve signal-to-noise.

It is evident that the practical application of equipment to a power line must involve some estimate of line noise to evaluate what transmitter power is needed. To this end the author should indicate what transmitter power he recommends for the various operating ranges. In many applications the problem of interference caused by the use of high transmitter power will more than offset the advantages of the increased range obtained with the high power.

It is indicated that the operating ranges given in the paper apply to the transmission of tones. If one tone fully modulates the transmitter then all the sideband energy is devoted to one tone. However, when several tones modulate a transmitter, the per cent modulation that can be devoted to each tone is lessened by the number of tones transmitted. This sharply reduces the transmitted power associated with each tone. If we assume these ranges apply when the full capability of the transmitter is used for a single tone, this rating must be reduced as additional tones are added; since the equivalent power for each tone must reduce as $1/n^2$ where n is the number of tones. This would be a 20 decibel reduction in operating range when ten tones are used.

In addition, it would have been worthwhile for the author to have devoted more of his paper to the proposed use of four tones below the voice range for telemetering channels. It is relatively easy for the harmonics of these tones and the sum of these tones to appear in the voice range during the amplification, modulation, demodulation, and amplification of the combined voice and tones after and before the separation filters. The magnitude of the tone interference in the voice band is proportional to the per cent distortion in the audio range. If one assumes that a given carrier channel has as little as 5 per cent distortion, then the harmonics of these tones may only be down 26 decibels in the voice range. That would mean that there would be tones at 580 cycles (two times 290) and 806 cycles (two times 403) due to harmonic distortion.

However, there is a still more serious source of interference.[1] One can make a fairly safe assumption that the intermodulation distortion due to the addition of tones would be at least twice the per cent distortion. Based on our previous assumption of 5 per cent distortion, this would mean that a tone of 693 cycles (the sum of 290 and 403) would appear in the voice band and be only 20 decibels below the level of the fundamental tones. If one assumes that any one of the fundamental tones is 15 decibels below the normal voice level, these interfering tones would be about 35 decibels below the voice level. If this level of interference were of random noise character, it might be tolerable based on the judgment of a user, but a single tone or combinations of single tones would have to be 45 to 50 decibels below the voice to be tolerable based on tests we have made. This means that a complete carrier-current channel must have less than 2 per cent distortion to meet this objective. It is practically impossible to maintain as low a distortion as 2 per cent in a carrier channel as applied to a power line, especially when it is combined with speech signals which vary in intensity from time to time. The preceding assumptions which lead to the conclusion are very conservative, and an actual case can easily be less favorable. Discussion by the author is invited on this topic.

Also, in discussing the repeating of audio tones from carrier channel to carrier channel, the same harmonic distortion and modulation products become additive from channel to channel and work to the disadvantage of successive usage of this principle. The author indicates the use of equalizing amplifiers, involving filters at the by-pass points, and it would seem very desirable to

have excellent automatic volume control or automatic level control at these points to hold modulation of succeeding transmitters to a safe level, so excess distortion could not occur. Also, what safeguards would be taken against voice levels causing overmodulation of the transmitter when involved in transmitting both tones and voice, and thereby causing severe harmonic distortion of the audio tones? When any form of limiting occurs, intermodulation distortion increases as the square of per cent distortion and would become very severe in this type of operation. Filters can be provided to separate the telemetering and communication circuits so long as they remain in separate frequency groups.

The points raised in this discussion involve interference of the telemetering tones in the voice channels at a point where the filters are powerless to eliminate the spurious frequencies from the telemetering circuits in the voice frequency group.

The author described a method of transmitting several readings from several outlying points to a central point on a system by means of single side band transmission. Related to intermodulation distortion described previously, is the problem of receiving and detecting all these signals in a common receiver. Line attenuation changes in this case would not affect all signals alike and some safeguards would have to be taken to prevent serious distortion in the common receiver. If an automatic volume control is used to avoid distortion, this would allow strong signals to blank out the weak ones. The author should describe this method more fully.

In describing the double frequency scheme for combining relaying, telemetering, and voice communication, it is not clear how local blocking is prevented when an internal fault occurs. From the information presented, the protective relays would have to control both the voice transmitter and the relay transmitter and require something more than normal in the way of control. Perhaps the equivalent control is provided some other way.

In reviewing the various possible carrier circuits for telemetering, the author did not mention the use of narrow-band frequency-shift carrier equipment for telemetering. This method is now being widely used on both telephone lines and power lines at 5–20 kc for telephone lines, and 90–200 kc and 30–90 kc for power lines. However, a paper is already scheduled to be presented at a later date which will describe this system.

REFERENCE

1. AN ANALYSIS OF THE INTERMODULATION METHOD OF DISTORTION MEASUREMENT, W. J. WATTES, W. R. HOWLETT. *Proceedings*, Institute of Radio Engineers (New York, N. Y.), volume 36, number 4, April 1948, pages 457–66.

M. A. Bostwick: Discussions by Thynell, Thomas and Lynch will be answered as a group as they all objected to classification of telemetering apparatus into three types namely: voltage, current, and impulse. This classification is advantageous as it tends to shorten the discussion of all types of circuits without omitting pertinent information. It is noted that Thynell states that "frequency type systems have been completely ignored," while Thomas and Lynch object to inclusion of frequency systems in the impulse classification.

The problems that are encountered in transmission of each type of signal, which have been mentioned by Thomas and Lynch, are of lesser importance. They should be considered, but are easily solved. This is borne out by experience. A highly accurate telemetering installation on the Union Electric Company of Missouri system, which employs impulse duration transmitters to control narrow-band audio frequency tone transmitters was recently described by R. G. Meyerand. Since the transmission of impulse rate signals is not of necessity limited to frequencies below that of the frequency-type telemetering transmitter, it is obvious that the problems encountered in transmitting these signals are similar.

In general, it can be stated that the width of the band of carrier frequencies that is used in a given channel will determine the amount of information that can be transmitted with a given accuracy, in a given period of time. This applies to all types of telemetering transmitters, impulse-duration, impulse-rate or frequency types. Consequently, the inclusion of these various transmitters in one classification was considered desirable.

Lunge calls attention to telemetering systems that were listed in the references, and states that, "Without an improved receiver it is difficult to understand how either accuracy or speed of response of the telemetering system could be increased." This is no doubt a misstatement. Accuracy involves both transmitter and receiver. Furthermore, the use of electronic controls permits use of higher impulse rates and increased speed of response, as he has pointed out.

Kenefake calls attention to a number of design details that are well known. Since the information primarily affects design, not use of the equipment, it was not considered to be properly a part of this paper. Very little data are available that accurately fix noise levels on power circuits. Because of this, the capabilities of the various types of carrier transmitters have been listed with factors of safety to assure operation through a quiet line when using a ten-watt transmitter. Larger factors of safety are used for carrier relay control circuits.

The filters, that are used to separate telemetering and voice circuits, are designed to give ample attenuation. Narrow band telemetering transmitters that were mentioned by Kenefake, were listed as crystal-controlled narrow-band equipment in the paper.

Economics of Long-Distance Energy Transmission

R. E. PIERCE
FELLOW AIEE

E. E. GEORGE
FELLOW AIEE

THE economic location of steam-electric generating stations is receiving intensified study, not only because of the tremendously increasing demand for electric energy, but because of changes that have taken place and are still occurring in the relative effect of certain primary controlling factors. These include changes in the relative effects of location and cost of alternate fuel supplies, cost of transporting alternate fuels, and cost of transmission of electric energy.

It has been rather usual practice to locate plants at adequate natural water supplies and adjacent to fuel sources or transportation, with transmission of the energy electrically to the load. If the transmission of electric energy produced from fuel involved distances in the order of 200 miles, the economics of this practice was questionable. Even the economics of transmitting electric energy produced from water power for distances of 200 to 300 miles was not too favorable unless the amounts of power to be transmitted were relatively large and competing fuel costs relatively high.

This paper presents comparative summaries of the cost of transmission of energy in the form of coal, gas, oil, and electricity. Relative costs of the three fuels near their sources are shown in Figure 7 for the period 1922 to date, illustrating the extent to which the relationship between fuel costs has changed within the last few years. From these data it is apparent that consideration must be given, in the economic choice of steam power plant location, to present day costs of fuel at the nearest sources and present day costs of transmission of the fuels versus the cost of transmitting electricity. The problem is analyzed only under average or typical conditions, but the results give an indication of the need for basic studies in each particular situation and give a fair approximation of the answer for average conditions.

The study of any particular problem involves many fields of engineering and it is therefore expedient to analyze only the basic considerations in this paper, which is devoted chiefly to the transmission of fuel energy. This paper concerns itself entirely with the straightaway hauling of large amounts of energy over long distances.

Electric Transmission

For a long time it has been generally considered that the high-voltage transmission line is the preferred means of carrying energy. This is largely due to its flexibility in distribution and conversion rather than any inherent economic superiority in transmitting large blocks of energy on a straightaway basis.

Analysis shows that the major factor affecting a comparison of costs of high-voltage transmission between 1915 and 1945 has been a doubling of permissible line loading under ordinary operating conditions. Technical improvements in telemetering, automatic load control, high-speed protection and reclosing, excitation, lightning protection, and the close co-ordination of these factors have permitted loadings of high-voltage lines close to the stable limits set by steady-state conditions. These increases in operating capacities have in large measure offset the increase in investment costs during the last 30 years.[1]

Basic transmission line designs have not changed materially in the last 30 years, except for improved lightning protection, and transmission line costs in 1945 were not much higher than after World War I. There has been a tendency to use larger conductor sizes and to use higher current densities than in the early days of high-voltage transmission. The cost of terminal equipment such as oil circuit breakers, transformers and synchronous condensers has increased and is responsible for some increase in the overall cost of electric transmission.

Transmission cost may be placed on a mileage basis. A study made in 1945 by one of the authors developed a cost of firm high-voltage transmission expressed by the formula

$$\text{mills per kilowatt-hour} = 0.50 + \frac{(0.47 \times \text{miles})}{100}$$

In 1947 Crary and Johnson[2] presented data from which can be developed a similar formula for transmission cost:

$$\text{mills per kilowatt-hour} = 0.30 + \frac{(0.47 \times \text{miles})}{100}$$

These formulas show substantial agreement in spite of independent assumptions as to line loading, losses, voltage, fixed charges, and so forth. Both of these formulas are for 50 per cent load factor and 1945 price levels in the United States.

The unit cost is substantially independent of transmission voltage, and of the amount of power transmitted, provided both are high. All figures for electric transmission in this paper (and in the Crary and Johnson paper[2]) assume optimum loads and voltages for various distances (200,000 kw or more on long hauls).

Figure 1 shows estimated costs of

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R. E. Pierce is consulting electrical engineer, and E. E. George is electrical engineer with Ebasco Services, Incorporated, New York, N. Y.

The authors wish to acknowledge the assistance of associates in Ebasco Services Incorporated in the preparation of this paper, particularly S. B. Flagg, mechanical engineer and E. P. Kramer, rate consultant, in the assembly of basic data.

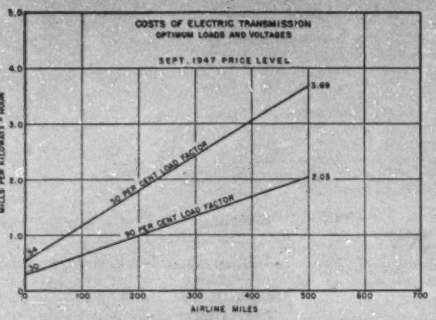


Figure 1. Costs of electric transmission

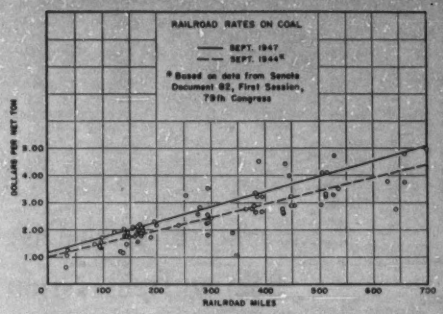


Figure 2. Railroad rates on coal

electric transmission, including fixed charges, losses, operation and maintenance, as of September 1947 price levels, for 50 and 90 per cent load factors. The increase in costs of transmission between 1945 and 1947 was estimated at about 35 per cent. Expressed by formulas, these costs are

For 50 per cent load factor

$$\text{mills per kilowatt-hour} = 0.54 + \frac{(0.63 \times \text{miles})}{100}$$

For 90 per cent load factor

$$\text{mills per kilowatt-hour} = 0.30 + \frac{(0.35 \times \text{miles})}{100}$$

Competitive Means of Transmitting Energy

Ordinarily, one thinks of hauling coal by rail and transmitting power electrically as the most typical competitive methods. However, this is only a small part of the picture. In a number of economic studies for electric and gas utility systems, oil refining companies, chemical companies, and transportation systems, various methods of transporting energy have been studied and compared including the following.

1. Coal by rail
2. Coal by ship
3. Gas by pipe line
4. Oil by pipe line
5. Oil by rail
6. Oil by tanker
7. Electricity by transmission line

In some cases the published class tariffs of common carriers may be assumed to accurately represent the cost of transporting fuel in moderate quantities and at commercial load factors to a power plant, but there is frequently evidence that the company contemplating construction of a large base load power plant might do better to seek special contract rates, or construct its own pipe line, because the published class tariff may be based on low-load factor operation for a multiplicity of customers with low diversity. As a lower limit of gas and oil pipe-line charges for very high-load factor, large-scale, long-haul operation, the estimates for large pipe lines given herein are fairly representative. These estimates assume private ownership and operation and maximum continuous use.

In comparison of various methods of transporting energy annual load factor is important, because fixed charges are greater than operating expenses, except possibly in the case of rail hauls. In the use of published coal tariffs of common carriers, the tariffs are assumed to be sufficiently independent of load factor to permit comparison on an energy basis in the charts shown in this paper.

Transportation of Coal

Figure 2 shows a scatter diagram of freight rates of coal, with dollars per net ton plotted against railroad miles between mine and destination. The individual rates plotted are for September 1944[3] but the solid straight line drawn to approximate the average relation between freight rates and mileage includes increases up to September 1947 but does not include the increases authorized late in 1947 and early in 1948. For this particular commodity there is a wide discrepancy in cost of transportation per ton. The spread in rates for any particular mileage is not due in any great degree to the geographic location and consequent difference in territorial level of freight rates, except possibly in mountainous areas, such as Virginia and West Virginia. Some of the rates are influenced by truck and barge competition. Rates under favorable conditions may be $.40 per ton under the average line, while rates under unfavorable conditions might well be $.50 to $1.50 a ton higher.

Due to the spread in rates it is imperative to analyze local conditions for the final answer in any particular case in which the freight rate on coal as determined from Figure 2 seems to be competitive with other means of transmitting energy.

No summary of costs of hauling coal by ship or barge is developed herein, but there is frequently keen competition between hauling coal by water and substitute haul by rail for the inland river and coastal routes. Such situations also necessitate detailed analysis.

Transportation of Gas

Figure 3 is a diagram of pipe line charges for natural gas. Since there are few common carriers for this service, the costs of transportation have been obtained by taking the wholesale industrial schedules of the gas utilities[4] and subtracting an estimated average cost of production and gathering at the sending end. For this analysis, $.08 per thousand cubic feet has been assumed as the sending end cost and has been deducted from the delivered cost to arrive at the cost of transmission. There is a wide spread in these rates. This might be expected since rates for both tariff and special contract types of services are included. Most of the tariff rates are for interruptible service, requiring oil standby service.

Because most of the gas utilities are engaged in retail operations as well as pipe line operation, there is no assurance

that the customers' rates reflect the portion of the total cost assignable to bulk pipe line operation. Some of the rates are probably promotional and some of them are based on long-term supply contracts which cannot be renewed without increase in cost. However, the long-haul contract rates for interruptible service to utilities check fairly well with the estimated cost for a large gas line under private operation, handling gas at maximum capacity at 100 per cent load factor (marked by an X on Figure 3). Apparently interruptible rates are nearly independent of load factor.

Transportation of Oil

Figure 4 shows a similar diagram of pipe-line tariffs on crude oil,[6] with dollars per barrel plotted against airline mileage between the gathering station and destination. There is comparatively small spread between the rates for any given mileage, and the average line is therefore fairly indicative. Most of the rates selected for the graph are those involving only one pipe-line company for each delivery, since this is more representative of competitive conditions. Some of the pipe line companies are common carriers without refineries or other operations and of necessity they establish tariffs proportional to their actual cost of pipe line operation. The load factor applying to typical pipe-line operation is unknown. An estimated cost, based on published data, for the Big Inch Lines at full capacity and 100 per cent load factor, assuming private operation is shown by the X on Figure 4. In many pipe lines, crude oil and lighter refinery products are handled interchangeably, but ordinary fuel oil for power plant use is too viscous for long haul by pipe line. Therefore oil for power plant or ship fuel requires a terminal refinery near the power plant, unless tanker haul is geographically feasible.

The handling of oil by rail in tank cars is somewhat of a retail operation as compared with pipe-line delivery. It is generally limited to points where there are no pipe lines and where there is insufficient traffic to justify a pipe line.

The hauling of oil by tanker is much cheaper than other means but is limited by geography to river and coastal terminals. Probably the best summary of costs of transporting oil is that given in the pamphlet "Economics of the Petroleum Industry," by Joseph E. Pogue of the Chase National Bank. Two pertinent quotations follow.

"For liquids available in sufficient volume with a concentrated market, the pipe line affords the most efficient form of overland transportation. While capital costs are substantial, rights-of-way are not expensive, the operation of the system is automatic to a high degree, movement is continuous, and there is no problem of two-way traffic or return movement of empty facilities. Because of these advantages and the technical proficiency of modern pumping systems, the average cost of pipe line transportation probably does not exceed four mills per ton-mile, which contrasts with an average cost of movement by rail of approximately eight mills per ton-mile. Thus no natural competition can persist between oil pipe lines and the railroads."

"Because of automatic loading and bulk, the oil tanker represents the ultimate in transportation efficiency, exceeding that of the pipe line. Comparative costs per ton-mile are approximately 8.3 mills by rail, 3.2 mills by pipe line, and 1.25 mills by tank vessel. In consequence the tanker competes with the pipe line and diverts as much traffic as the geographic pattern permits."

The costs quoted previously refer to conditions in 1939, but present-day tariffs for 300- to 500-mile pipe-line haul are still around 3 or 4 mills per ton-mile. By using terminal storage tanks, oil tariffs are made on a volume or weight basis, nearly independent of load factor.

Comparative Costs of Transporting Energy

Before making comparisons between transportation of energy by various means, it should be pointed out that comparison between transmission costs only for gas, oil, and coal are likely to be misleading because there is so much difference in the costs of these fuels at their sources. This is particularly true under present conditions.[8] In the past, it was usual to assume that there was no choice of fuels, or that a definite selection had already been made, and that the only comparison necessary was between transportation of the particular fuel and electric transmission. The picture is very much different now from that 10 or 15 years ago. Gas and oil are now transported by pipe line in large quantities for distances over 1,000 miles. Mechanization of coal mining in the future may offset increasing labor costs. Fuel oil is no longer a by-product derived from crude oil in a ratio fixed by the natural chemical composition of the oil. Developments in petroleum chemistry now provide considerable flexibility in the percentages of the various refinery products. Therefore the future price of crude oil is likely to be highly susceptible to technical and economic factors in the whole petroleum industry. The low cost of transporting oil and gas by pipe line as compared with the cost of hauling coal by rail together with the current progress in converting coal to gas or oil may still further upset economic relations between the three fuels in the next few years.

It should likewise be kept in mind that the present activity in construction of gas and oil pipe lines is likely, within the next few years, to greatly increase the number of localities which have these fuels available in quantity at competitive prices.

Figure 3. Pipe line rates on gas

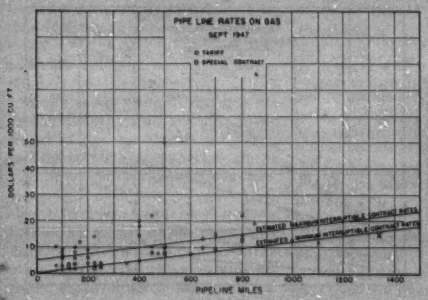
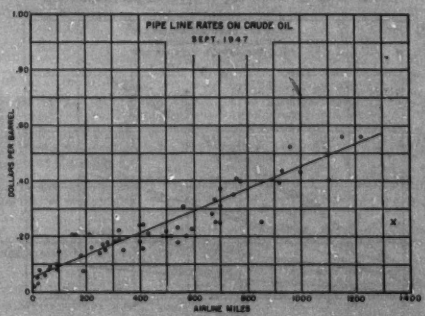


Figure 4. Pipe line rates on crude oil



Comparisons between coal, gas, oil, and electric transmission are made easy by using the following nominal conversion factors and efficiencies.

1 barrel of oil = 6,250,000 Btu = 500 kilowatt-hours
1,000 cubic feet of gas = 1,000,000 Btu = 80 kilowatt-hours
1 ton of coal = 25,000,000 Btu = 2,000 kilowatt-hours
1 kilowatt-hour = 12,500 Btu (modern steam plant)

Figure 5 shows costs for transporting energy by various means with mills per kilowatt-hour plotted against airline miles. It is assumed that pipe-line miles are substantially airline miles. To obtain the costs in cents per million Btu, multiply the mills per kilowatt-hour by eight.

All data for transportation of fuel are commercial rates, using the average lines of Figures 2, 3, and 4. The average for gas has been taken as halfway between maximum and minimum rates of Figure 3. The electric transmission costs are the same as those shown in Figure 1. These electrical costs reflect a 35 per cent increase in price level from 1945 to September 1947, compared with approximately 15 per cent increase in freight rates, pipe line tariffs, and so forth.

Although Figure 5 is based on estimated averages, it answers in part the basic question as to how electric transmission compares with other means. At 50 per cent load factor, the high-voltage transmission line and the coal car are generally competitive for short distances only.

The situation for base load plants is highly competitive. Load factors of 96 per cent or more are involved. At 90 per cent load factor (base load operation) electric transmission may well be as cheap as coal for any distance, and as cheap as gas for distances up to 100 miles.

Transmission line costs are almost exactly inversely proportional to load factor, whereas the charges for pipe line transportation as shown by Figures 3 and 4 are largely independent of load factor, being based on use of storage, substitute fuel or curtailment of service.

It should be noted that the cost of transmitting energy by gas pipe line is two or three times as high as the cost of transmitting energy by oil pipe line.

Figure 6 is the same as Figure 5 except that total costs are shown on a capacity basis instead of an energy basis.

Figure 6 facilitates comparisons with hydro power. There is no choice of means of transmission when using hydro power. Such simple and tangible expressions for transmission cost easily demonstrate that so-called "cheap power" is not cheap if it has to be transmitted electrically to a distant market. The slope of the electric transmission cost lines in Figure 5 and Figure 6 gives directly the difference in cost of transmission for different distances, and is approximately 0.63 mill per kilowatt-hour per 100 miles or $2.78 per kilowatt per year per 100 miles, both for 50 per cent load factor. At lower load factors these unit cost differentials per 100 miles would be even greater. It is readily apparent therefore that with the investment cost of a hydro plant generally greater than for a similar size steam plant, hydro power would be more costly than steam power by the time it was delivered a few hundred miles away. In areas of low fuel cost, hydro power transmitted even shorter distances would be more expensive than steam power.

Costs of Fuels at Sources

Figure 7 shows the relation between prices of coal, gas and oil for the years 1922 to date. Values plotted in this figure are the average yearly realized prices of bituminous coal at the mines,[7–9] fuel oil at the refineries in the south Texas oil fields[10,11] and natural gas at the wells.[8,12,13] Coal and gas annual figures are averages for the entire United States, compiled by the Department of Interior. Oil figures are from the Petroleum Almanac. The values for the third quarter of 1947 are estimates, based on spot prices. Old contract prices, which largely determine realized prices are still considerably lower, but in a rapidly rising market, spot prices are considered more indicative of new contracts.

From Figure 7 it will be seen that the prices of coal and gas, at the sources, were about equal, on an energy basis, prior to 1934. Since that time, the price of coal has risen very rapidly and coal now costs about three times as much as gas for the same amount of energy. In those areas which were not too far distant from both coal and gas sources, the two fuels were competitive for a long time. This is no longer true, and as will be shown later, gas can be carried by pipe line a considerable distance and still be competitive with coal near the mine. Under such conditions the choice of fuels can no longer be made on an arbitrary or noncompetitive basis.

Prior to 1945 fuel oil cost about twice as much as coal or gas on an energy basis. This explains why fuel oil was used only for standby purposes, except in a few plants located a long way from the coal and gas fields and yet cheaply accessible by tanker from the California, Texas, Mexico or Venezuelan oil fields. Today the spot price of crude oil is about three times the cost of coal and about nine times the cost of gas, thus making it very expensive for standby service if such service is required many hours in a year. Present conditions in the oil industry are

Figure 5. Costs of transporting energy-mills per kilowatt-hour

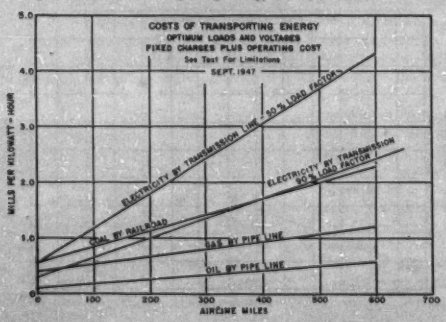
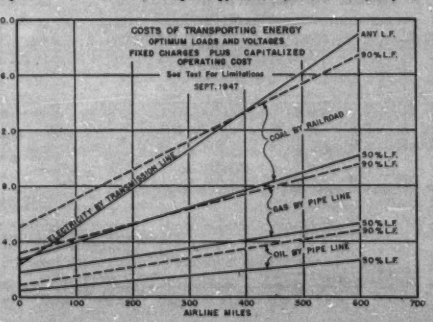


Figure 6. Costs of transporting energy-dollars per kilowatt per year



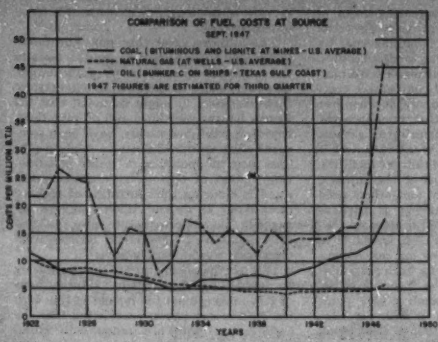


Figure 7. Comparison of fuel costs at source

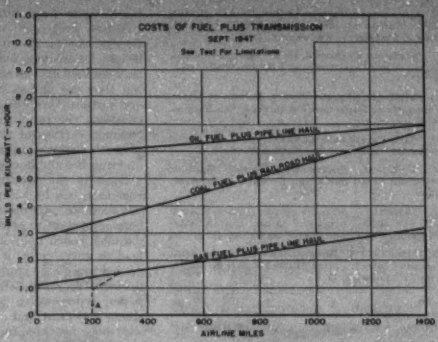


Figure 8. Costs of fuel plus transmission

reported to be largely due to shortages of pipe line and refining capacity. These should improve when sufficient steel becomes available and when more foreign oil fields have pipe lines completed to tidewater terminals.

It therefore appears that the choice of normal fuel and of standby fuel for a power plant justifies extensive study for each particular project and that this situation will continue for some time to come.

Delivered Costs of Energy

Figure 8 shows a comparison of present day "delivered costs" for the three fuels, coal, oil and gas. Delivered cost is intended to denote only the cost of the fuel itself and the cost of its transportation. This is obtained by combining the information on 1947 fuel costs at the source, Figure 7, with that on cost of transporting the respective fuels, Figure 5.

The information on Figure 8, delivered fuel energy, and Figure 1, transmission of electric energy, with certain adjustments for differences in other production costs could be combined in several ways to show comparative costs of electric energy produced from alternate sources and transmitted different ways and different distances to a given load center.

As one example, assume that it is desired to compare gas which could be transported by pipe line 300 miles to a large load center with hydro power located 100 miles from the load center. By superimposing on Figure 8 a line representing the cost of electric transmission as shown by the dashed line on the chart, it is determined that, aside from differences in other fixed charges and production expenses, the hydro energy would be worth 0.34 mill per kilowatt-hour, based on the cost of the alternate gas fuel. (See *A* on chart). In this comparison it is assumed that electric transmission is at 50 per cent load factor, limited by the characteristics of the hydro project. Typical charges for pipe-line transportation of gas for utility service (interruptible contracts) have been considered to be applicable.

Theoretically, a power plant might be located at the fuel source, at the load, or at any point in between.

Practically, a low-load factor steam plant should be located at the load (because the incremental cost of electric transmission is higher than other means of transmission) except for distances under 200 miles with coal fuel from a mouth-of-mine plant. Such a plant (with coal delivered to the bunkers from mine cars—no rail switching charges involved) would have about a half mill saving to offset the incremental cost of electric transmission. A high-load factor power plant burning coal would have to be studied in detail to determine the best location. If feasible, mouth-of-mine location would generally be most economical.

For oil or gas fuels, pipe line transmission is usually the most economical, regardless of distance or load factor.

Conclusions

1. Technical improvements in the art have in large measure offset increases in cost of electric transmission.

2. Cost of electric transmission for optimum loads and voltages can be expressed as a linear function of power and distance.

3. Cost of fuel transportation can be expressed as a linear function of energy and distance.

4. Electric transmission at low load factor is competitive with hauling coal by rail for only short distances.

5. Electric transmission at high load factors (base load plants) is competitive with hauling coal by rail for any distance (up to several hundred miles).

6. Electric transmission is generally much more expensive than transporting energy by gas or oil pipe line.

7. The cost of transmitting energy by gas pipe line is several times as high as by oil pipe line.

8. Tanker transportation of oil costs only about half as much as pipe line transportation.

9. The costs of fuel and its transportation to load centers are usually the dominant factors in determining the market value of hydro energy.

10. Coal and gas are no longer competitive, either close to both sources or several hundred miles away. Gas may be carried several hundred miles and still compete with coal near the mine.

11. Fuel oil is now too high for use except for standby service, unless tanker haul is available and other fuel sources are several hundred miles distant.

12. A mouth-of-mine steam plant is economical if the plant load factor approaches base load conditions.

13. If gas or oil is to be used for fuel, a power plant location near the load will almost always be most economical.

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Discussion

Philip Sporn (American Gas and Electric Service Corporation, New York, N. Y.): The authors are to be complimented on the work that they have done on a very important and difficult subject. The relative economics of different sources of energy, each transmitted by the best technical means available, needs further discussion and as a prelude to such discussion, further work needs to be done on the development of what the underlying economics and potentialities are. The authors have made a contribution of note in that direction, but a great deal more work still remains to be done. The authors have, however, perhaps under-estimated the remaining gaps. A point, too, that needs to be remembered is that in their studies, costs and tariffs have been mixed. Thus, transmission line costs, based upon fixed and operating costs of facilities necessary to provide electrical transmission, and cost of hauling coal by freight, based upon existing tariff schedules, may each be cost to the ultimate consumer, but they are not strictly comparable costs and certainly are not comparable when one considers the problems that may face the common carriers of the country as a result of continuing rises in cost of labor, materials and supplies and in cost of railroad facilities.

Some of the conclusions of the authors, therefore, can be accepted and others need considerable modification. It is particularly important to point out that broad conclusions, based upon averages, are liable to lead to grave error, if they are not accompanied by specific studies for each particular project. As an illustration, Figure 8 of the paper, representing September 1947 data, shows a typical fuel cost in mills per kilowatt-hour for three fuels, one of them being coal coming in by railroad in a range of distance from zero to 1,400 miles. For coal fuel, the figures vary from 2.8 mills for at-mine location to 6.7 mills for a distance of 1,400 miles. This is based upon an average performance of 12,500 Btu per kilowatt-hour. However, on an actual system the weighted average data for the year 1947, where approximately 10,000,000,000 kilowatt-hours were generated by coal burning capacity, with coal delivered distances up to 200 miles, and with plants having performances running from 10,200 Btu to approximately 30,000 Btu per kilowatt-hour, the following weighted averages figures prevailed:

Btu per kilowatt-hour,	13,689
Fuel cost per kilowatt-hour,	2.14 mills

R. E. Pierce and E. E. George: The comments of Sporn are appreciated, especially in view of his long experience and interest in this subject.

The authors agree that pipe line tariffs and freight rates have been used as costs in the absence of any better information. However, while the paper calls attention to these instances, it might be noted that the figures for gas pipe lines and oil pipe lines, based on published tariffs, have been compared with estimated costs on modern pipe lines now in operation or under construction. Most of the electric utilities interested in securing transportation of fuel are more likely to depend upon concerns engaged in this business than to enter the business themselves. Therefore, tariffs rather than costs would usually govern.

It is felt that the figures of Sporn showing an average 1947 fuel cost per kilowatt-hour of 2.14 mills are not too far out of line with the data in our paper, considering that the costs Sporn quotes are undoubtedly based on contracts which would lag behind spot prices in a rising market. Whether one buys coal or mines it himself, his fuel costs for an additional plant requiring new fuel sources today will of necessity be much higher than average figures for an existing system.

Since the preparation of the paper in 1947, there have been extensive changes in the prices of fuels. These changes do not contradict the general conclusions. However, in the comparison in the second paragraph under Costs of Fuels at Sources, the figures given would read about as follows if brought up to the third quarter of 1948:

Approximate Costs of Power Plant Fuels at Sources

Gas	$.10 per million Btu
Coal	$.20 per million Btu
Oil	$.40 per million Btu

Development of the Transmission System From the Grand Coulee Power Plant

B. V. HOARD
MEMBER AIEE

E. E. HELWITH
ASSOCIATE AIEE

THE transmission system of the Bonneville Power Administration transmits the power from the Grand Coulee generating station and from the Bonneville generating station, both situated on the Columbia River, to three major load areas in eastern Washington, western Washington, and western Oregon (Figure 1). The Bonneville generating station has had its full capacity of 570,000 kw installed since December, 1943. Since April, 1948, Grand Coulee has had nine of the ultimate of 18 generators installed, and plans call for rapid installation of the remaining nine generators. With 18 generators at Grand Coulee, the actual peak output will be in excess of 2,160,000 kw.

A series of studies has been made, and it appears that by proper system arrangements this amount of power can be handled economically by the construction of ten 230-kv lines and three to four 115-kv lines. The ten 230-kv lines will carry approximately 2,000,000 kw with an average transmission distance of 180 miles, while the 115-kv lines will handle the remaining power supplying loads near the vicinity of the generating station. With all generators peaking this plan will result in an average load at the sending end of each 230-kv line of about 200,000 kw per line. The system is unique in that the very large amount of generation concentrated at one location makes it possible to take advantage of multiple transmission circuits to a load area. For such a system as compared to one which, for example, has just two lines to a load area, the loss of any one transmission circuit is relatively a smaller percentage of the total transmission capability. Hence each line in the system can be loaded both normally and during emergencies to a considerably greater amount than would otherwise be possible in the two circuit system, without any sacrifice in transient stability, thereby providing greater economy of design.

In view of the fact that the three load areas served by the Bonneville Power Administration are widely separated, several questions naturally arise: should all lines be solidly bussed at the Grand Coulee switchyard; should lines and generators supplying each load be completely segregated; or should a compromise arrangement be used which connects busses through some reactance? This paper, as one of its major objectives, presents the interesting results of the investigation made to find the answers to these questions. A second major objective of the investigation was to determine system arrangements which would result in a high value of transmission capability and operating flexibility without requiring circuit breaker duties greater than it was possible to manufacture.

In order to carry rapidly growing system loads, plans call for rapid installation of Grand Coulee generators and the building of transmission circuits as indicated in Table I.

To obtain a more general picture of the problems encountered it may be well to explain that the Grand Coulee power plant and dam were constructed as a multiple-purpose irrigation and power project under the control of the United States Bureau of Reclamation. The Bonneville Dam and powerhouse, near Portland, having navigation features, were built and are operated by the United States Army Engineers. The Bonneville Power Administration is designated as the transmission and marketing agency for the electric power output of both dams, as well as other dams in the Columbia Basin which are now planned or being built.

From the standpoint of the ultimate development of the Columbia River and its tributaries, the Coulee and Bonneville projects are the first two of a series of many to be constructed in the Columbia River Basin, having a possible economical development of something over 10,000,000 kw. Of these additional dams, initial steps have been taken for the construction of McNary Dam on the Columbia River (966,000 kw peaking capability). Figure 1 shows the 1948 transmission system and generating stations geographically with probable major transmission system additions up to 1952, and with the McNary, Detroit, and Hungry Horse Dam sites indicated.

Somewhat farther in the future are the Foster Creek and Hells Canyon developments, the four plants on the lower Snake River and Albeni Falls on the Clark's Fork River. The development of a transmission system for Grand Coulee is only a part of a more comprehensive program for an over-all transmission system which must be planned to handle the output of all these future stations, and preliminary studies are now being made on this problem.

With respect to immediate system

Table I

As of	Number of Generators Installed	Number of 230-Kv Transmission Circuits	Capability of Station 120,000 Kw per Unit
December, 1948	9	6	1,080,000
December, 1949	12	8	1,440,000
December, 1950	15	9	1,800,000
December, 1951	17	10	2,040,000
December, 1952	18	10	2,160,000

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B. V. HOARD and E. E. HELWITH are both with the Bonneville Power Administration, Portland, Oreg.

planning, the development of a transmission system which will handle approximately 2,200,000 kw of Grand Coulee generation, with necessary switching facilities and operating flexibility at the Grand Coulee switchyards, and which will provide transmission system stability and over-all economy, becomes a major undertaking. The switchyard arrangement determines how the lines and generators will be connected together, and the type of connection will produce varying effects on system transient stability as well as on steady-state loadings. It will determine oil circuit breaker interrupting duties, which are much higher than could be handled by past circuit breaker designs of 3,500,000 kva. These and other requirements had to be achieved at the most reasonable cost consistent with reliable service. Investigation of how these requirements could be met by the various plans became a joint responsibility of engineers of the Bonneville Power Administration system and of the Bureau of Reclamation.

Those aspects of the problem involving location of system loads and transmission line capabilities, line interconnections at Grand Coulee, power transfer capacities, and system relaying and stability, were of vital interest to Bonneville Power Administration and were worked out with the help of Bonneville Power Administration's a-c. network analyzer. Problems of physical plant layout, electrical control and switching facilities, and design, relaying and construction of the switchyards, were the province of the Bureau of Reclamation.

Figure 1. December, 1948, transmission system with probable 230-kv additions up to 1952 and some future power site locations

Preliminary Design

Original design called for isolated bus sections with two parallel lines and two 108,000-kva generators per circuit. Circuit breakers of 2,500,000 kva were the largest available. This arrangement would produce line loadings of 108,000 kw and obtain reliable operation over each pair of long 230-kv lines. If three generators were to be isolated on two long lines normal loads of 162,000 kw per line would occur which was considered excessive from a transient stability standpoint.

System Development

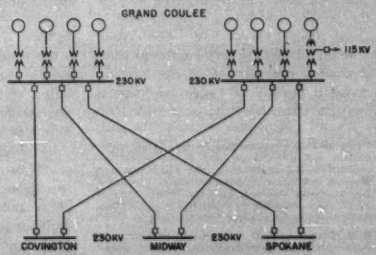
The first lines built were two 230-kv circuits to Portland via Midway and Bonneville Dam, and a line to Seattle (Covington substation), Figure 1. In addition a tie line was constructed between Seattle and Portland, which supplied major intermediate loads at Chehalis and Longview substations. It became necessary for reliability purposes to close the 230-kv loop at the Grand Coulee switchyard rather than synchronize through 607 miles of line. Then, two 230-kv lines were built to Spokane to supply war loads. It was necessary to synchronize these lines also at Grand Coulee switchyard with the lines to the Puget Sound and Portland areas. Otherwise relatively large synchronizing surges would have to take place through other lower voltage ties owned and operated by other agencies. It was not possible to isolate the Spokane load because this would prevent the importation of power from Montana into the western areas where it was critically needed.

After the addition of the fourth Grand Coulee generator to the common 230-kv bus, the 2,500,000-kva circuit breakers became overstressed. Circuit breakers with 3,500,000-kva interrupting rating became available and an interleaved line system, Figure 2, with two busses, four generators, and three lines per bus section was developed to carry through the war emergency. Synchronizing between busses was obtained through the interleaved lines. This arrangement has been described in a paper by Hoard and Bills.[1] It served well under the handicap of shortages of material, and a shortage of generation in some areas. Normal line loadings of over 170,000 kw have been attained with transient stability. This bussing arrangement will continue to operate at Grand Coulee until November 1948, when plans for major changes will be put into effect. These plans are described in a companion paper prepared by engineers of the Bureau of Reclamation.

Ultimate Comprehensive Design

Meanwhile, extensive transient stability studies on the Bonneville Power Administration network analyzer established the fact that through development of a proper high-voltage grid network the ultimate system could be designed for an average normal loading of 180,000 to 200,000 kw per 230-kv line, with the aid of suitable circuit breaker facilities and high-speed relaying, while still preserving transient stability through all reasonable fault conditions. An investigation of several high-voltage bussing schemes was undertaken to separate those having the most favorable transient stability properties for consideration in the more detailed design. By development of a bussing scheme which would give transient stability at line loadings of 200,000 kw, the peak output of 16 generators at 125,000 kw per generator could be handled over ten 230-kv transmission lines, assuming the spare generator in operation

Figure 2. Interleaved line scheme placed into operation during the war



and two generators reserved for 115-kv supply, instead of the 15 or 16 230-kv lines considered in the original design.

A large saving over the original design (using isolated busses) could be made since five lines would be eliminated. Allowing some increase in cost to develop the grid system at Grand Coulee and at step-down substations, due to larger circuit breakers, an over-all saving of from $20,000,000 to $25,000,000 is indicated. Obviously, isolating the system into units of two lines and two generators without synchronizing ties between busses at the generating station is not an economical solution nor is it practical to place three generators on two lines, as the emergency line loading would be 360,000 kw after a faulted line section had been removed, and there would be no possibility of maintaining transient stability. Another major disadvantage of the isolated system is the lack of operating flexibility when more or less power must be supplied to a particular load area. Also, other existing ties between major load areas which are owned and operated by other agencies would have to be opened to prevent synchronizing surges normally, and prevent instability during emergencies.

Bussing all generators and lines without reactance between bus sections supplies the greatest operating flexibility; however, the circuit breaker short-circuit duty for 3-phase faults near Grand Coulee becomes well over 11,000,000 kva and still higher circuit breaker duties are calculated for line-to-ground faults.[3] Besides the high circuit breaker duty, there are some disadvantages to straight solid bussing which will be discussed later. The design which is finally selected usually ends up as a compromise, but it should meet the following requirements:

1. Maintain steady-state and transient stability under anticipated distribution of ultimate peak loads.

2. Provide adaptability of the transmission network to accommodate large shifts in load without difficulty and without severe unbalancing of line loadings. This feature arises because the Bonneville Power Administration transmission network serves as the main interconnecting medium to coordinate the differing seasonal requirements of various other utilities for over-all water storage and utilization. Other shifts in line loading will occur as new generating stations are connected into the grid.

3. Either limit the short-circuit interrupting duties of circuit breakers or make certain that sufficiently large circuit breakers can be obtained to meet the requirements. It was considered desirable to obtain circuit breakers of high enough interrupting rating to permit high-speed reclosing following the occurrence of line faults. Another important factor was that single-line-to-ground faults in the vicinity of Grand Coulee, with all transformer neutrals grounded, would exceed 3-phase faults by approximately 15 per cent.

4. Limit the normal generation on any one bus section without sectionalizing circuit breakers to three generators (360,000 kw) so that in the event of the loss of a bus section, spinning reserves would pick up and carry the load. The 360,000 kw corresponds to approximately 10 per cent of total generation in the interconnected systems of the Northwest area in 1948.

5. Provide facilities to divert the power output of up to five generators to water pump operation over considerable periods without seriously unbalancing the transmission line loadings or the ability of the system to withstand transient shocks.

6. Provide for supplying a large block of future off-peak power to potential research plants.

7. Provide adequate general operating flexibility and maintenance facilities.

In short, the method to be used when connecting lines and generators together at Grand Coulee actually influenced the problem of the arrangement of a considerable part of the entire transmission network and vice versa.

Types of System Considered

Three basic types of transmission system were studied in relation to the foregoing requirements:

1. A system of parallel line pairs fed from busses electrically connected through various amounts of reactive impedance closed into a ring bus. As a special case, a solid bus is obtained when the reactive impedance becomes zero.

2. Extension of the interleaved line[1] idea to 18 generators.

3. Parallel line pairs fed from isolated busses.

Numerous arrangements and modifications of the foregoing types were investigated, totaling perhaps 30 schemes in all. Early in the study, because of economic and operating flexibility reasons already discussed, the isolated bus arrangement (scheme 3) was discarded.

From the standpoint of a preliminary analysis, scheme 1, with no reactance, that is a solid bus, appears to have special advantages over scheme 2 because of the ability to transfer reasonable amounts of power through the bus without shift of generation and with reasonable independence of load distribution on outgoing lines, but if this bus requires sectionalizing circuit breakers to meet the 360,000 kw limitation per bus, its cost will be greater than that of scheme 2. Still greater costs are involved for scheme 1 when reactors are used between bus sections, also the installation of reactors in the ring bus has the disadvantage that a future bottleneck may develop if the current carrying capacity is not sufficient.

One starting point for the ring bus (scheme 1) is found in the results of a paper by Bills and MacArthur,[2] in which transformers with multiple windings, Figure 3, furnish sufficient isolation between high-voltage busses to limit faults, and at the same time allow power to shift from bus to bus and furnish a path for synchronizing torques.

A modification was worked out for a five-section ring, using some standard 2-winding transformers and some transformers with duplex high-voltage windings, Figure 4. Each tie bank of transformers would have a rating for each of two high-voltage windings of 170,000 kva and 260,000 kva for the low-voltage winding, with an impedance of 38 per cent between high-voltage terminals, and 20.5 per cent from each high terminal to the low terminal on a 170,000-kva base. This starting point was based on earlier analyzer studies showing low circuit breaker duty and excellent stability for single-circuit faults on the system. When it appeared that 3-winding transformer manufacturing difficulties and costs would be excessive, and there would be installation difficulties in the existing left powerhouse, this scheme was discarded in favor of bus tie reactors as indicated on Figure 5. Electrically, the synchronizing torques produced by high-voltage bus tie reactors or by 3-winding transformers can be made substantially equivalent through selection of reactance values.

Transient stability studies were made for single-circuit faults, a major portion of which were located just outside the Grand Coulee switchyard, the worst spot in terms of system shock within the area affected by Grand Coulee bus design.

Fault clearing time was taken as 4.5 cycles at the near line terminal and nine cycles at the far end of the line. Although 3-phase faults are infrequent, they were used for simplicity, and for the fast clearing times considered, the shock of a 3-phase fault is very little greater than for the more prevalent 2-phase-to-ground faults.

When reactors were used, it was found that for faults near Grand Coulee the system was more stable, that is, a lower value of angular difference occurred between generator rotors at the point of maximum swing than when the reactors were bypassed (a "solid bus"). It will

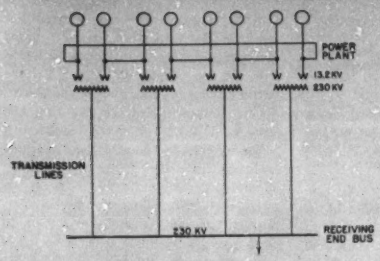
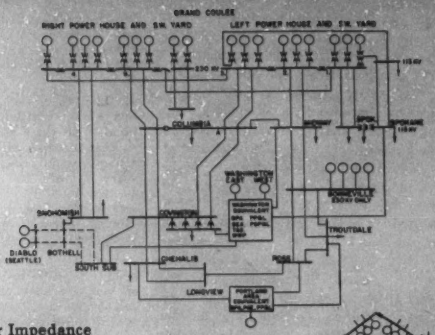


Figure 3. Three-winding transformer ring bus scheme

Ring developed on low-voltage side



be recalled that these bus-tie reactors introduce two opposing tendencies:

1. If the fault could be cleared in zero time, the shock to the system would be identical to that introduced by removal of the line section by switching only. In that event bus reactors would be a pure liability, for they introduce additional reactance between portions of the system.

2. However, when the fault is cleared in a finite time, the opposing effect occurs, namely, during the fault period the reactors decrease the short-circuit current, maintain higher voltage and higher load on unfaulted busses and increase the synchronizing power between machines, so that the generators unload less, accelerate less, and therefore stability is increased. Even for the short clearing time of 4.5 cycles this effect is sufficiently great that it appears worthwhile to introduce some reactance between busses to improve stability, apart from its value in reducing circuit breaker duty.

For the system studied, the effect of bus reactors was to aid stability for line and bus faults at the sending-end bus, but reduce stability for faults at substations one line section distant from the generating station. However, a net advantage is obtained since faults on or near the generator bus are the more severe and determine the stability limit of the system. These results were found for 4.5-cycle clearing of bus faults and 4.5- and 9-cycle sequential clearing of lines.

Optimum Reactor Impedance

Insofar as the Coulee bus design could influence stability, it had been planned to secure transient stability both for single-circuit faults and for bus faults at Grand Coulee. As the study progressed, it began to apprear that the effects of multiple paths in the high-voltage grid were greater than had been anticipated and that by lowering the impedance of the bus-tie reactors and arranging to take care of the increased circuit breaker

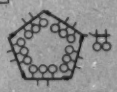


Figure 5. Five section ring reactor bus as developed for Grand Coulee and Bonneville Power Administration system with provision for sixth (Stub) bus for increased load

(Circuit breakers omitted)

Figure 6. Effect of reactor size on circuit breaker duty and relative stability

Five section ring bus with five bus tie reactors, 17 generators, no pumping

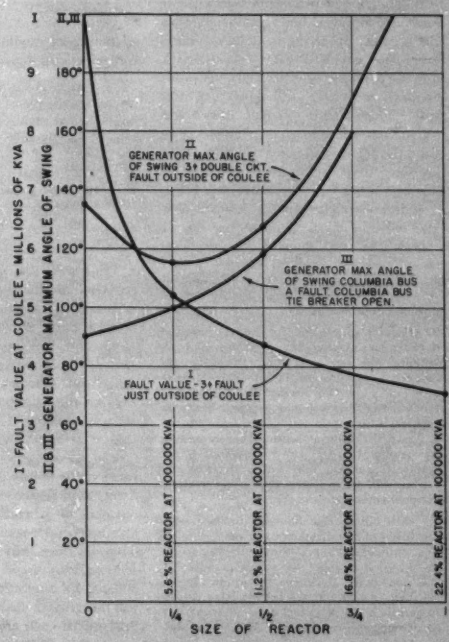
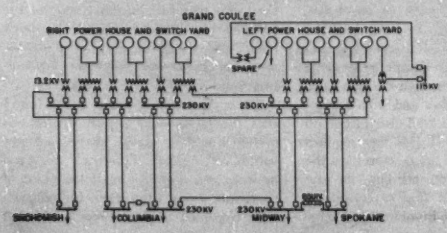
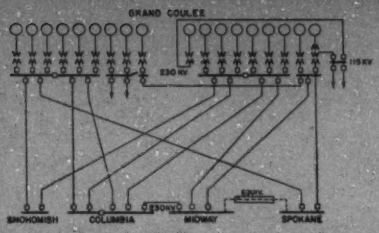


Figure 4 (below). Three-winding transformer ring bus with duplex high-voltage windings

(Ring developed in high-voltage side through transformer windings)





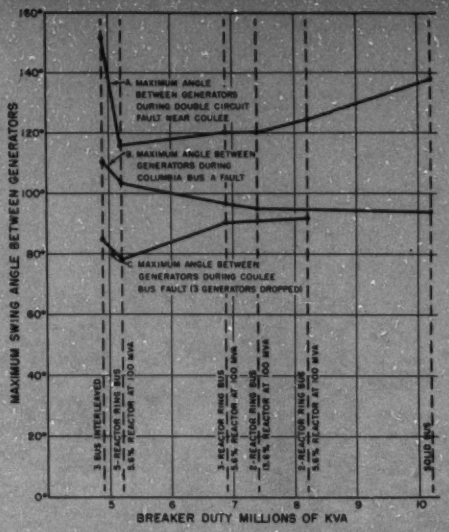


Figure 7 (above). Three bus interleaved scheme

Balance of system as in figure 5

Figure 8. Relative circuit breaker duty and stability of bussing schemes at Grand Coulee

Seventeen generators, no pumping

duty resulting therefore, it would be possible to raise the criterion to maintain stability following the occurrence of simultaneous double-circuit faults. Such faults have been infrequent on existing parallel lines of single-circuit construction, but experience on the double-circuit tower line from Grand Coulee to Spokane has shown that their frequency is greater when parallel lines are placed on double-circuit towers. Some of the future lines to be built out of Grand Coulee will be of double-circuit construction because of lower over-all cost, and because of crowded right-of-way at certain locations; therefore simultaneous double-circuit faults had to be seriously considered.

A double-circuit fault on parallel lines became the criterion for which to design, and the impedance of bus reactors at Grand Coulee was changed to determine the value which would give optimum system stability. The index of comparison was taken as the maximum angular difference reached during the transient oscillation following fault clearing, between machine rotor angles for the most leading and most lagging generator group in the Northwest interconnected systems. Reactor impedance was varied to minimize this transient maximum angle. Curve II of Figure 6 shows that for these faults best results were obtained with reactors of about one quarter of the original impedance value (used for a single circuit fault). It will be seen that this gives somewhat better stability than with a solid bus (zero per cent reactance), with a maximum angle during swing of 115 degrees for the quarter size reactors (5.6 per cent reactance on a 100,000-kva base), compared with 136 degrees for the zero per cent reactors or solid bus. The equivalent angular difference prior to the fault was 53 degrees steady state, from prefault load studies. Curve III of Figure 6 shows that for bus faults on bus *A* at Columbia substation (see Figure 5) "best stability" is obtained with zero per cent reactances at Grand Coulee, but it will be observed that the angular swing is always less than for a fault near Grand Coulee. The significance of these angular differences as a measure of relative stability has not been fully evaluated because the maximum angular spread to which the generators can swing and recover is not shown, and it is tedious to make this determination for numerous faults in a complicated system. However, it is assumed here that a lesser angle of maximum swing indicates a greater margin of stability.

From Figure 6 it appears that approximately the same degree of stability is obtained by either a solid bus or a 12 to 13 per cent reactor, but a 6 per cent reactor gives a greater degree of stability. The reduced oscillation with optimum reactors must be evaluated as a greater margin of safety with which to meet the fault condition during a time when some other line or facility is out of service. Or system loads may materialize which produce greater unbalance in line loadings than those assumed, or greater unbalance will occur when future generating stations are added to the grid, with resultant decrease in stability margin.

Curve I of Figure 6 shows the value in kilovolt-amperes of 3-phase faults (asymmetrical component included) at Coulee plotted against size (impedance) of reactor. This shows 5,200,000-kva fault for the quarter size or 5.6 per cent reactor, and over 10,000,000 kva for the solid bus. The necessary circuit breaker rating should be greater than the fault figures shown here to allow for future system expansion, and any increase of line-to-ground fault values over 3-phase faults. It now appears that where high-speed reclosing is to be applied to the newer high interrupting capacity 230-kv circuit breakers being developed, no derating may be required for one high-speed opening and reclosing cycle.

Reactor Loading Studies

Studies were made shifting generators from bus to bus to observe the effect on line loadings. This effect was inconsequential for the 5.6 per cent reactor impedance previously found desirable for optimum stability. The power added to a bus went for the most part back through the reactors to the original busses and lines, with only a small increase in loading in the parallel lines leading from the bus in question. This is not surprising when it is realized that the value of bus reactance is the equivalent of only 35 miles of 230-kv line. From this result the conclusion may be drawn that where generation supplied to a bus differs from the natural line loadings out of that bus, the ring reactors should be capable physically of carrying the full difference. This is an important consideration also in view of the fact that the output of four or five generators will be used at Grand Coulee during the summer for irrigation pumping.

Interleaved Scheme

The study next took up the interleaved type of bussing arrangement (scheme 2). Several arrangements were investigated, but the one which gave the best results is shown in Figure 7. It will be observed, for simultaneous faults on lines occupying the same right of way, that each of the faulted lines in the interleaved scheme will be connected to a separate bus at Grand Coulee. This type of fault was more severe from a stability standpoint than when it occurred in the solid bus scheme, but slightly less severe from a circuit breaker duty standpoint than when it occurred in the five bus ring reactor scheme. These results are indicated in Figure 8.

In view of the low circuit breaker duties and passable stability characteristics of the interleaved scheme, an investigation of costs was made by the Bureau of Reclamation engineers. This indicated that the cost would probably be $500,000 less than for a five bus-ring reactor scheme. However, after further study it was abandoned because it did not allow for the condition that system loads develop in unexpected places which require flexibility to take care of such growth and to transfer power from one area to another. This flexibility is not inherent in this scheme as it is in the reactor or solid bus schemes. Another important disadvantage was that the load dispatcher would have trouble determining in advance what line loadings would occur for any given system loads and generation connected to each bus, and during emergencies when a faulty line section was cleared, even an oracle would have trouble determining how the unbalance in line loading would distribute.

Other Bussing Arrangements

A three reactor ring bus and a two reactor ring bus were investigated also. Summarizing, the stability was slightly better than for the solid bus and slightly poorer than for the five reactor ring bus. The circuit breaker duty increased rather rapidly as the number of reactors was decreased. These results are indicated also in the curves of Figure 8.

Conclusions

1. The development of a high-voltage transmission grid shows considerable overall economy in comparison with a number of relatively small isolated systems each fed from a separate bus at Grand Coulee.

2. Circuit breaker duty in the grid system is much greater than for the isolated systems, however, the additional operating flexibility which is available and the greatly increased line loadings obtainable without the loss of transient stability during emergencies, fully justify increased switchyard costs.

3. Of the various switchyard arrangements which will provide for a transmission grid the solid bus provides the greatest operating flexibility, and where high-speed clearing of faults is obtainable provides for a high degree of transient stability, but circuit breaker interrupting duty is relatively higher than for other arrangements.

4. An interleaved line scheme is the most economical and does not require exceptionally high circuit breaker duties but lacks operating flexibility and has a somewhat lower stability limit. Operation of a system with one or more lines out of service is especially difficult with the interleaved scheme.

5. A ring-type bus with sectionalizing circuit breakers provides for bus faults or for failure of equipment to clear a line fault, without losing the whole station or without losing the necessary synchronizing power between other bus sections and other parts of the transmission system.

6. When a small amount of reactance is introduced between the sections of a ring bus, a major reduction in circuit breaker duty results and a higher degree of transient stability is obtained. However, additional equipment and somewhat greater costs are involved. In addition, at Grand Coulee when a considerable part of the generation is supplying power for irrigation pumping, large transfers of power through bus reactors raises the question whether the reactor carrying capacity would always be adequate for unforeseen future operating conditions.

7. If reactors are used their benefits from the standpoint of transient stability are very high when 8-cycle circuit breakers are used but benefits decrease as the clearing time is lowered. For 3-cycle circuit breakers as used at Grand Coulee the stability benefits are relatively small.

8. In view of the foregoing considerations a five section ring bus has been selected with circuit breakers of 10,000,000-kva interrupting rating, the largest which could be developed in the relatively short time available, and with provision in the switchyard to install reactors at a future date when conditions warrant.

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Discussion

W. M. Leeds (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The authors are to be congratulated on an excellent presentation of a very comprehensive system study and analysis of transient stability during short circuit conditions for a variety of possible bussing schemes at Grand Coulee power plant. The results of fault current calculations are of special interest to circuit breaker manufacturers, particularly if the values exceed existing standard interrupting capacity ratings. In addition to the special 10,000,000-kva 230-kv circuit breakers that will be required at Grand Coulee, what other ratings above 3,500,000 kva are indicated for nearby points on the transmission system?

With the considerable background of high power laboratory and field testing which has been accumulated in recent years, no difficulty would be anticipated in building 230-kv 3-cycle circuit breakers for either 5,000,000 or 7,500,000 kva. However, in the interest of economy, a careful survey of future requirements of the larger power systems is desirable to avoid the introduction of an unnecessary number of different circuit breaker interrupting capacity ratings.

B. V. Hoard and E. E. Helwith: The following two major points were raised by Leeds in his discussion.

1. In addition to the special 10,000,000-kva 230-kv circuit breakers at Grand Coulee, what other ratings above 3,500,000 kva are indicated for nearby points on the Bonneville Power Administration transmission system?

2. In the interest of economy it would be desirable to avoid introduction of an unnecessary number of circuit breaker interrupting ratings.

Preliminary studies indicate that for several of the intermediate stations a circuit breaker of from 5,000,000 to 7,500,000 kva will be needed within the next five to ten years on the Bonneville Power Administration system. The upper value of interrupting duty is obtained at substation locations near which another major power development is contemplated, and which will be tied into the substation. Studies indicate also that at future generating stations, several of which will have installed capacities of from 500,000 to 1,000,000 kva, the switchyards will require circuit breakers of the order of 7,500,000-kva interrupting rating.

The problem of standardizing on a number of circuit breaker ratings, which will fulfill the requirements of all of the larger power systems, but in the interests of economy avoids the introduction of an unnecessary number of different interrupting ratings, is a difficult one. Values of 3,500,000, 5,000,000, 7,000,000 and 10,000,000 kva interrupting rating or of 3,500,000, 6,000,000 and 10,000,000 kva correspond approximately to geometric ratios and may be

used. Perhaps, in view of the fact that recent tests indicate the circuit breakers rated 3,500,000 kva really are capable of interrupting up to at least 5,000,000 kva when in good operating condition that two ratings of 5,000,000 and 10,000,000 kva, respectively, should be the correct answer since both sizes are, or will be completely developed very soon.

We are not in a position to judge what the relative manufacturing costs will be, but it appears to us that if the tank, oil, bushings, and operating mechanisms for a 7,500,000 and a 10,000,000-kva circuit breaker are identical for one manufacturer, but for another manufacturer may be identical for a 2,500,000, 3,500,000, 5,000,000, and 7,500,000-kva circuit breaker, that large differences in cost for some ratings will result even though the selling prices must be commercially competitive.

From a commercial standpoint, circuit breakers of 5,000,000, 7,500,000, and 10,000,000 kva rating have already been sold by various manufacturers. Up to the present time there has been a rather large price increase in the step from 5,000,000 or 7,500,000 to 10,000,000 kva. Taking all these factors into account, it is our opinion that the ratings of 5,000,000 and 7,500,000 kva are both very definitely in the picture. On the other hand, the 10,000,000-kva circuit breaker development for Grand Coulee is not fully completed. It is our hope that future tests will show these circuit breakers will be 12,500,000 or even 15,000,000-kva circuit breakers, for a larger size than 10,000,000 kva actually is needed at Grand Coulee. For this reason, to our way of thinking, the 10,000,000 kva rating is less definite at the present time than the 5,000,000 and 7,500,000-kva values.

Measurement of Interference From Radio-Frequency Heating Equipment

GEORGE H. BROWN
FELLOW AIEE

THE continued and increasing usage of radio-frequency power generators for industrial heating processes has alerted the radio industry to the possibility of interference with existing communication channels. The Federal Communications Commission, guided by recommendations of interested manufacturers and by committees of the AIEE and the Institute of Radio Engineers, has established rules regarding electronic power generators, setting aside frequency bands for their use and establishing limits of radiated field intensity for the spurious harmonic radiations and for the cases where the generators are operated outside of the established channels. It is the purpose of this paper to describe some measurements of the fields from typical equipment and to discuss some of the pertinent factors which should be considered in connection with these measurements.

Fields from Generators Used for Dielectric Heating

Dielectric heating, the heating of inherently poor electric conductors, is usually accomplished by using generators operating at frequencies above two megacycles. In most cases, the frequency is far in excess of this figure. Induction heating of metals is generally carried out at frequencies below 0.5 megacycle. Thus the two types of heating are carried out in frequency ranges separated by the standard broadcast band. We shall first examine the case for dielectric heating equipment. The various factors which lead to spurious currents in ground wires, conduits, and oscillator frames have already been described and will not be discussed in this paper.

The extraneous currents which cause undesirable radiation may be resolved into vertical and horizontal components. In an extremely idealized case, we shall assume that these currents are concentrated at a height h above a flat ground. The measuring point, at a distance d from the source, is at a height a above the ground. The vertically-polarized field intensity at the measuring point is

$$E_V = \frac{E_D}{d}\left[\frac{2\epsilon_0(h+a)}{\sqrt{\epsilon_0-1}} + j\frac{4\pi ha}{\lambda} - j\frac{\epsilon_0^2\lambda}{\pi(\epsilon_0-1)}\right] \quad (1)$$

and the horizontally-polarized electric field intensity at the same measuring point is

$$E_H = \frac{E_D}{d}\left[\frac{2(h+a)}{\sqrt{\epsilon_0-1}} + j\frac{4\pi ha}{\lambda} - j\frac{\lambda}{\pi(\epsilon_0-1)}\right] \quad (2)$$

where

ϵ = the dielectric constant of the earth
σ = the earth conductivity (mho-centimeter)
f = frequency (cycles per second)
ϵ_0 = a complex dielectric constant $= \epsilon - j\frac{18\cdot 10^{11}\sigma}{f}$
λ = the wave length
E_D = the direct or free space field

These equations are taken directly from a paper by Charles R. Burrows[1] where in his equation 28 we substitute $2\epsilon_0/\sqrt{\epsilon_0-1}$ for his a-ib to obtain equation 1, and use $2/\sqrt{\epsilon_0-1}$ for his a-ib to obtain equation 2.

In equation 1, the middle term is the only one of importance when both h and a are large compared to the wave length. When either h or a becomes small, the first term becomes necessary, and when both h and a are small it is necessary to consider the last term. This latter term is the Sommerfeld surface term. In equation 2, the last term is generally always negligible. While a lengthy word picture could be painted of the manner in which the various terms behave, it is recommended that the interested reader substitute numerical values to get the feel of the situation.

Since the direct field E_D varies inversely as the distance d, we may rewrite equations 1 and 2 as follows.

$$E_V(\text{microvolts per meter}) = \frac{K\lambda\sqrt{W}}{d^2}\left[\frac{2\epsilon_0(h+a)/\lambda}{\sqrt{\epsilon_0-1}} + j\frac{4\pi ha}{\lambda^2} - j\frac{\epsilon_0^2}{\pi(\epsilon_0-1)}\right] \quad (3)$$

and

$$E_H(\text{microvolts per meter}) = \frac{K\lambda\sqrt{W}}{d^2}\left[\frac{2(h+a)/\lambda}{\sqrt{\epsilon_0-1}} + j\frac{4\pi ha}{\lambda^2} - j\frac{1}{\pi(\epsilon_0-1)}\right] \quad (4)$$

where W equals the watts radiated. The constant $K = 22\times 10^6$ when h, a, λ, and d are all measured in feet. When h, a, and λ are measured in feet and d is expressed in miles, the constant K is 0.789. It should be noted that in the case of radiation from an electronic generator the watts indicated in equation 3 differ from those in equation 4 since the vertically-polarized currents are not necessarily equal to the horizontally-polarized currents.

Before proceeding with further consideration of these equations, we shall examine the mode of variation of the complex dielectric constant with frequency. Let the dielectric constant of the earth ϵ be 15, with σ equal to 0.5×10^{-4} mho-centimeter. The resulting computations are shown in Table I. It may be seen that at the high frequencies, the conductivity is practically negligible, while at the low frequencies the conductivity term predominates.

As a specific example of the information which may be extracted from equations 3 and 4, we shall examine the bracketed term in each equation for a frequency of 27 megacycles and with a

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GEORGE H. BROWN is with the Radio Corporation of America, RCA Laboratories, Princeton, N. J.

dielectric constant of 15. The results of calculation are shown in Tables II and III. Since the receiving antenna height is usually taken as 10 feet, the bracketed term of equation 3, displayed in Table II, varies from 5.59 to 7.36 as the transmitting antenna height changes from zero to 20 feet. These values substituted into equation 3 show that to produce 10 microvolts per meter at one mile the radiated power which produces the vertically-polarized field must have a value between 0.0025 and 0.00388 watt.

A similar calculation based on Table III shows that between 0.0328 watt and 5.48 watts are required to produce a horizontally-polarized field intensity of 10 microvolts per meter at one mile.

The amounts of power indicated are surprisingly small and may lead one to believe that it is a difficult task to reduce spurious radiations to the required values. To illustrate the magnitude of the field intensities encountered in practice, measurements of field intensity were carried out on a laboratory oscillator located in the RCA Laboratories. The oscillator, operating at 27 megacycles, delivered 825 watts to a wood load which was six inches square and five inches thick. The input to the plate of the oscillator was 1,725 watts. The oscillator was in a first-floor room of the brick and steel-reinforced building at the point shown in Figure 1 where the lines A and B meet. Measurements of vertically-polarized and horizontally-polarized field intensity were made at a number of locations along the two lines, using an RCA Type *301-A* field intensity meter, with the measuring antenna mounted on a tripod ten feet in height.

The resulting values of field intensity are shown in Figure 2. The line A extended across a level open field, while the line B was obstructed by a new excavation, huge mounds of earth, and an apple orchard. As one might expect, the field intensities along the line A are substantially higher. The vertically-polarized components of field intensity average four to five times higher than the corresponding horizontal components. It is interesting to note that the vertically-polarized values obtained on the line A extrapolate to 16 microvolts per meter at 1,000 feet and to the extremely low value of 0.6 microvolt per meter at one mile. It is very likely that the steel reinforcement of the building contributes to the effective shielding of this oscillator. The effect of the building structure may be illustrated by another example.

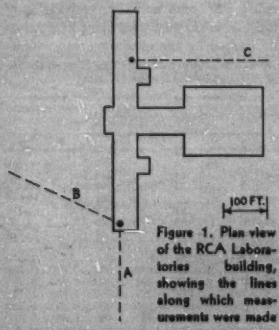

Figure 1. Plan view of the RCA Laboratories building, showing the lines along which measurements were made

A radio-frequency sewing machine[9] used for the bonding of plastic sheet was placed in the middle of a flat open field. The machine was raised about six feet above the surface of the earth, and the plate voltage adjusted to normal operating conditions. The frequency was 42.5 megacycles. With the receiving antenna ten feet above ground, measurements were made along a radial line. The results for both the vertical and horizontal components are shown in Figure 3. The upper curve shows the vertical component, which is far stronger than the horizontal component. The vertically-polarized component extrapolates to 15 microvolts per meter at one mile.

The sewing machine was next moved into the laboratory building and placed in the spot formerly occupied by the 27-megacycle oscillator. Measurements were then attempted along the line A of Figure 1. The three crosses near the bottom of Figure 3 show the values of vertically-polarized component. The horizontally-polarized components were of the same order of magnitude as the receiver noise and were not recorded. These short-range measurements, when extrapolated, show that the field intensity at one mile now reaches the unmeasurable value of 0.3 microvolt per meter.

The sewing machine was next placed in a double-shielded cage across the hall. Then it was not possible to find even a trace of signal 50 feet from the radiating source.

The Transition to Low Frequencies —the van der Pol Relation

When the frequency is below 500,000 cycles, the range commonly used for induction heating, the conductivity term in the expression for the complex dielectric constant predominates. This effect may be seen in Table I. Then equation 2

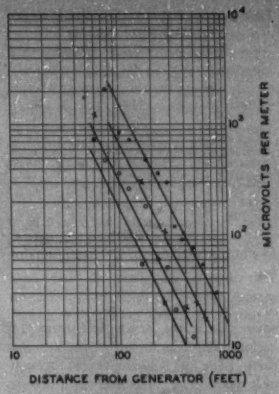

Figure 2. Measurements of field intensity radiated by the 27-megacycle oscillator

• Vertically-polarized intensity along line A
O Horizontally-polarized intensity along line A
× Vertically-polarized intensity along line B
□ Horizontally-polarized intensity along line B

becomes of no importance in comparison to equation 1 and need be considered no further. In addition, the last term in equation 1 is the only term that need be retained.

Since

$$\epsilon_0 \approx -j\frac{18\cdot 10^{11}\sigma}{f} = -j60\sigma\lambda \tag{5}$$

equation 1 becomes

$$E_V = \frac{E_D}{d}\cdot\left(\frac{60\sigma\lambda^2}{\pi}\right) \tag{6}$$

Since E_D is the direct field from the radiating source which varies inversely with the distance, we may conclude that at low frequencies the field intensity decays with distance in an inverse square relation just as did the high frequencies. Our common experience with propagation from broadcast stations does not bear out this conclusion. In fact, it is well known that waves at broadcast frequencies propagate and decay inversely with distance from some distance and then gradually approach the inverse square relationship at great distances. Doctor Burrows[1] has extended his treatment to take care of the case of vertical antennas on the surface of the earth and arrives at formulas which are in substantial agreement with experience. For our purpose of exposition of the subject, the treatment by B. van der Pol[2] seems excellent. He shows that the field intensity at the sur-

Table I. Variation of the Complex Dielectric Constant With Frequency

f(cycles)	ϵ_c	$\lvert\epsilon_c\rvert$
100,000	15-j900	901.
500,000	15-j180	180.5
2,000,000	15-j45	47.5
10,000,000	15-j9	17.5
27,000,000	15-j3.33	15.35
100,000,000	15-j0.9	15.02
500,000,000	15-j0.18	15.

face of the earth due to a vertical radiator is

$$E_V = E_D f(\rho) \tag{7}$$

where E_D is the direct field from the radiator and from its mirror image in the earth, while $f(\rho)$ is a complicated function of a quantity ρ, which van der Pol calls the "numerical distance." It may be remarked that E_D in equation 7 is twice the value of E_D in equation 6.

The numerical distance is

$$\rho = \frac{\pi d}{60\sigma\lambda^2} \tag{8}$$

when d and λ are expressed in centimeters and σ is the conductivity of the earth, measured in mho-centimeters. For greater convenience, we may express the wave length λ in meters and the distance d in miles, with the conductivity still measured in mho-centimeters. Then

$$\rho = \frac{0.842d}{\sigma\lambda^2} \tag{9}$$

Table IV shows $f(\rho)$ as a function of ρ over a useful range. B. van der Pol also states an empirical relation for the function $f(\rho)$, that is,

$$f(\rho) = \frac{2+0.3\rho}{2+\rho+0.6\rho^2} \tag{10}$$

which agrees extremely well with the values shown in Table IV.

For large values of ρ, corresponding to great distances from the radiating source, equation 10 reduces to

$$f(\rho) = \frac{1}{2\rho} \tag{11}$$

When equations 8 and 11 are substituted into equation 7, the field intensity at a distance becomes

$$E_V = \frac{E_D}{d}\cdot\left(\frac{30\sigma\lambda^2}{\pi}\right) \tag{12}$$

Thus we find perfect agreement between equations 6 and 12, since we have already noted that E_D in the latter is twice the value of E_D in the former.

Since we shall generally be measuring field intensity from induction heating equipment at distances less than one mile at frequencies lower than 500,000 cycles per second, a substitution in equations 9 and 10 reveals that $f(\rho)$ is substantially unity. In any event, we may express the field in terms of the radiated power, W watts, as

$$E_V(\text{microvolts per meter}) = 5{,}880\,\frac{\sqrt{W}\cdot f(\rho)}{d} \tag{13}$$

where d is measured in miles.

It is of interest to note that one watt of radiated power will produce a field intensity of 5,880 microvolts per meter at one mile. To hold the field at one mile to ten microvolts per meter or less, the radiated power must not exceed three microwatts.

Radiation from Closed-Circuit Currents at Low Frequencies

In the preceding section, we found that the field intensity dropped off practically as the inverse first power of the distance when the frequency was below 500,000 cycles per second. However, this is strictly true only when the distance to the measuring point is great in comparison to the wave length. At a frequency of 300,000 cycles, the wave length is in the neighborhood of 3,000 feet. Since we desire that the radiation from industrial oscillators be less than ten microvolts per meter at one mile, we must make our measurements at much closer distances if we are to obtain reliable data to prove in the equipment.

Table II

Bracketed Term of Equation 3—Vertical Polarization
Frequency = 27 megacycles
Dielectric Constant = 15

A(feet)	= 0	5	10	15	20
a(feet) 0	5.15	5.27	5.59	6.11	6.79
5	5.27	5.37	5.73	6.28	6.91
10	5.59	5.73	6.07	6.64	7.36
15	6.11	6.25	6.64	7.25	8.04
20	6.79	6.91	7.36	8.04	8.91

The radiation from induction-heating oscillators is caused by currents flowing in closed circuits or loops, such as the frame, doors, or even the tank circuit itself.

We shall first set up the equations for the fields near a single radiating loop placed near the surface of the earth, and then show how these equations are related to actual field measurements.

Let us imagine a loop antenna of area A, and carrying a current of I amperes, standing in a vertical plane. The plane of the loop runs in a north-south direction. Then at a point which lies a distance d straight east (or west) from the loop antenna, there will exist a magnetic intensity which points east. At this point, there is no north-south component of intensity, so that another receiving loop placed at this measuring point will be subjected to a maximum piercing of flux when the plane of the receiving loop is parallel to the first loop and of course at right angles to the radial line running from the radiating source to the receiving point.

There will be no electric field intensity at this measuring point. The magnetic intensity at this measuring point we shall designate as H_p, since here the two loops must be parallel to each other to obtain maximum coupling. This magnetic intensity is given by the expression

$$H_p = \frac{IA}{\pi}\left[\frac{1}{d^3} - j\frac{k}{d^3}\right] \tag{14}$$

where $k = 2\pi/\lambda$.

The radiation resistance of the radiating loop when placed in the vertical position and close to the earth is

$$R_r = \frac{640\pi^4 A^2}{\lambda^4} = 40\cdot k^4 A^2 \tag{15}$$

and the power radiated by the loop is

$$W = 40\cdot k^4 A^2 I^2 \tag{16}$$

Substituting equation 16 in equation 14, we find

$$H_p = \frac{2}{\lambda}\cdot\sqrt{\frac{W}{40}}\left[\frac{1}{(kd)^2} - j\frac{1}{(kd)^3}\right] \tag{17}$$

Now let us imagine that we could turn our radiating loop about a vertical axis so the plane of the loop now runs in an east-west direction. Then at the measuring point straight east from the radiating loop at a distance d, the magnetic intensity will point north and the receiving loop must lie in the east-west plane in order to be pierced by the maximum flux. Thus the two loops will be coplanar, and we

Table III

Bracketed Term of Equation 4—Horizontal Polarization
Frequency = 27 megacycles
Dielectric Constant = 15

A(ft.)	0	5	10	15	20
a(ft.) 0	0.0227	0.0765	0.148	0.219	0.292
5	0.0765	0.261	0.501	0.745	0.99
10	0.148	0.501	0.966	1.435	1.92
15	0.219	0.745	1.435	2.15	2.85
20	0.292	0.99	1.92	2.85	3.81

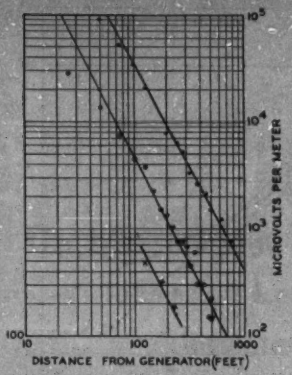

Figure 3. Measurements of field intensity radiated by the radio-frequency sewing machine

• Vertically-polarized intensity, with machine in open field

O Horizontally-polarized intensity, with machine in open field

× Vertically-polarized intensity, with machine in laboratory building

shall designate the magnetic intensity of interest in this condition as H_{cp} where

$$H_{cp}=\frac{2}{\lambda}\sqrt{\frac{W}{40}}\left[\frac{1}{2}\left\{\frac{1}{kd}-\frac{1}{(kd)^3}-j\frac{1}{(kd)^2}\right\}\right] \tag{18}$$

In equation 18, the factor 1/2 has been placed inside the bracket so that the coefficient of the bracketed term will be the same as that of equation 17.

The vertical electric field at this latter measuring point is expressed by the following equation.

$$\frac{E}{120\pi}=-j\frac{2}{\lambda}\sqrt{\frac{W}{40}}\left[\frac{1}{2}\left\{\frac{1}{(kd)^3}+j\frac{1}{kd}\right\}\right] \tag{19}$$

One may observe from equations 18 and 19, provided one has been subjected to the proper stimuli, that these expressions are identical with the expressions for the electric and magnetic intensities from a short electric dipole with the roles of the electric and magnetic intensities interchanged.

Table IV. The Function $f(\rho)$ as a Function of ρ, the Numerical Distance

ρ	$f(\rho)$	ρ	$f(\rho)$
0	1	10	0.061
0.05	0.979	11	0.0539
0.10	0.958	12	0.0485
0.25	0.899	13	0.0441
0.50	0.808	14	0.0405
1	0.656	15	0.0374
2	0.440	16	0.0348
2.5	0.363	17	0.0325
3	0.303	18	0.0305
4	0.215	19	0.0287
5	0.159	20	0.0272
6	0.123	25	0.0213
7	0.098	30	0.0176
8	0.082	40	0.0130
9	0.070	50	0.0103

The space relationship of these intensities with respect to the radiating loop is shown in Figure 4.

The bracketed terms of equations 17, 18, and 19 are shown in Figure 5. Curve A is simply an inverse cube curve for comparison purposes. Curve B is the bracketed term of H_p from equation 17. It may be seen that for distances less than 1/10 wave, this relation is inverse cube of the distance and approaches inverse square as the distance becomes great. Curve C is the bracketed term of H_{cp} from equation 18. For small distances, this curve is also inverse cube with values exactly half of the corresponding values of curve B. As the distance becomes large, curve C approaches the inverse distance relation. Curve D is the bracketed term of equation 19, starting as an inverse square curve and at great distances approaching the inverse distance relation.

As the distance becomes large, equations 18 and 19 become

$$H_{cp}=\frac{E}{120\pi}=\frac{1}{2\pi d}\sqrt{\frac{W}{40}} \tag{20}$$

or

$$E=\frac{30}{d}\sqrt{\frac{W}{10}} \tag{21}$$

In equation 21, the electric intensity is expressed in volts per meter when the distance d is measured in meters. When the distance is measured in miles and the electric field intensity is expressed in microvolts per meter, the equation becomes

$$E(\text{microvolts per meter})=\frac{5{,}880\sqrt{W}}{d} \tag{22}$$

The similarity to equation 13 is apparent.

Since we have no way of telling in advance how the effective loop of radiating current is oriented in an induction-heating oscillator, we might sample the magnetic intensity at a selected measuring point. That is, we might first place our receiving loop so it points at the transmitter. The reading obtained here would be H_{cp} and would tell us something about the component of the radiating loop current which produced H_{cp}. Next, we rotate the loop until it is perpendicular to the first position. Then our radial measuring line is perpendicular to the plane of the receiving loop. A reading here will give us H_p and will then yield the remaining component of the radiating loop current.

The measurement of the magnetic intensity is quite simple. The ordinary field intensity meter which uses a loop antenna is calibrated to yield readings of electric field intensity by making use of the fact that in a radiation field, where the wave is essentially plane, the electric and magnetic field intensities are in a constant ratio. This ratio is 120π, or 377, the characteristic impedance of free space. The voltage induced in the loop is always proportional to the magnetic intensity passing through the loop. This latter proportionality is true even when the fixed ratio between the electric and magnetic intensities no longer exists. Thus we may always obtain the magnetic intensity by reading the apparent electric field intensity on the meter and dividing this value by 377. The oscillator used to obtain data to illustrate these steps was an *RCA-15B* unit, converted to operate on a frequency of 289.5 kc. The output circuit contained a transformer with a multiturn primary and a single wide copper sheet for the secondary. This sheet terminated in a single turn of copper tubing which surrounded the load. The load was a steel cylinder, 1.25 inches in diameter and approximately 4 inches long. Cooling water ran through this steel cylinder. The rate of flow and temperature rise was observed. At the time the field intensities were measured, the following conditions prevailed.

Plate voltage—8,200 volts.

Plate current—1.72 amperes.

Plate input power—14,100 watts.

Water flow through load—1.09 gallons per minute.

Temperature rise—27.2 degrees centigrade.

Power into load—7,870 watts.

The oscillator was located on the second floor of the RCA Laboratories at the position where the radial line C begins in Figure 1. The work coil was then 20

Table V. Measured and Calculated Magnetic Intensities of the 289.5 Kc Oscillator

Distance (Feet)	Apparent Electric Field Intensity (Microvolts per Meter)	Measured H_p (Ampere-turns per Meter)	Calculated H_p
50	16,200	43.0×10^{-4}	75.8×10^{-4}
75	5,820	15.5	22.75
100	3,100	8.2	9.57
125	2,000	5.3	4.95
150	1,170	3.1	2.91
175	770	2.04	1.85
200	475	1.26	1.26
225	330	0.875	0.897
250	225	0.598	0.665
300	150	0.40	0.402
450	30 (unreliable because of receiver noise)		

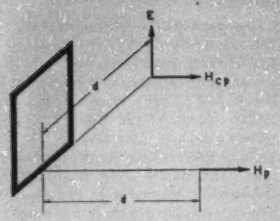

Figure 4 (left). The intensity relations with respect to a radiating loop

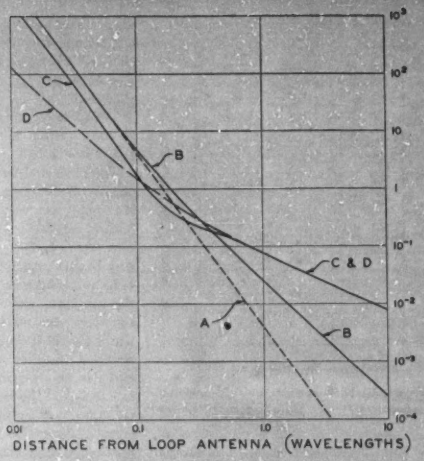

Figure 5

A. An inverse cube line
B. The bracketed term in H_p
C. The bracketed term in H_{cp}
D. The bracketed term in $E/120\pi$

feet above ground level. Measurements were made on the line C shown in Figure 1.

The field intensity meter was an RCA Type 308-A, using a loop antenna. At all points on the measuring line, maximum signal was observed when the receiving loop antenna was turned so that the loop was perpendicular to the line C. When the loop was pointed directly at the oscillator, no reading was observable on the meter. This condition could of course be interpreted as indicating that line C was exactly perpendicular to the plane of the radiating loop, an entirely fortuitous circumstance which had nothing to do with the choice of the line C as the locale of our measuring activities.

Readings were made at many locations along the measuring line. After plotting the readings and comparing to curve B of Figure 5, it was found that the experimental points gave the best fit to the theoretical curve when exact correspondence was made at a distance of 200 feet.

The manner in which Table V was constructed may be best explained by a numerical example. At 200 feet, the apparent electric field intensity indicated by the field intensity meter was 475 microvolts per meter or 0.000475 volt per meter. This latter number divided by 377 gives 1.26×10^{-6} ampere-turns per meter for the measured H_p. This value substituted into equation 14 yields a value of 0.84 for the product IA. This means that the radiating loop was effectively one square meter in area and carried 0.84 ampere of current. Equation 16 then shows that the radiated power is 3.82×10^{-6} watts. Substitution of this power into equation 22 yields a predicted electric field intensity at one mile on a line perpendicular to line C of 1.13 microvolts per meter.

The results of measurements on line C and the corresponding calculations are shown in Table V. The first column indicates the distance from the oscillator. The second column shows the apparent electric intensities registered on the field intensity meter, while the measured values of H_p are listed in the third column. The fourth column shows the calculated values of H_p, using 0.84 for the product IA.

It is unfortunate that time and circumstances did not permit explorations on other radials. However, the author hopes that the analysis given here may guide and assist other workers interested in these problems.

Conclusion

The author has attempted to illustrate the factors which are important in interpreting the measurements of field intensity from industrial oscillators operating in the high-frequency region. The transition of the analysis from the high-frequency solution to the frequencies of interest in induction heating is made by means of van der Pol's solution of the wave equation. The near fields of radiating loops are established and correlated with measurements. Estimates of radiated power from typical industrial oscillators are made, as well as extrapolations of the data to yield field intensities one mile distant.

The smoothness of the experimental data may be attributed to the fact that the RCA Laboratories building is surrounded by open fields, with no overhead wires. The low values of field intensity are probably due to the great shielding obtained by the building construction. In built-up industrial areas, greater field may be expected where overhead wires leave the buildings and great difficulty in obtaining reliable and consistent data may be anticipated in cities.

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No Discussion

The Measurement and Investigation of Ionization Level of Rubber Insulated Cables

R. C. GRAHAM
ASSOCIATE AIEE

E. K. DUFFY
NONMEMBER AIEE

W. P. FOSTER
ASSOCIATE AIEE

Synopsis: A review is presented to show the historical development of measuring techniques and the need for practical equipment and methods to test rubber insulated cables. A new concept for equipment design is presented together with descriptions of recently developed commercial testing units suitable for such application. Design of new and novel terminal and electrode apparatus for investigation of rubber insulated cables is described, together with data obtained from laboratory and production tests.

IONIZATION* may take place when air or other gaseous medium is subjected to sufficient electrical stress to cause actual electrical discharge or corona within the medium. In order for this phenomenon to exist in a so-called "solid" dielectric it is required that air spaces or voids must exist within or on the boundaries of the dielectric. An electrical device utilizing a solid dielectric would be considered ionization-perfect for all practical purposes if all voids and air spaces were eliminated internally and externally to the device. This perfection may be approached in various degrees depending upon the mechanical, physical, chemical, and electrical nature of the device, as well as the surrounding atmospheric conditions. Within devices such as capacitors, transformers, paper insulated cables and the like, another medium such as an oil or some other fluid may be introduced to appreciably replace the ionizable air spaces. Other factors such as mechanical design are also carefully considered to eliminate or reduce voids. However, within a rubber or extruded insulated cable we must rely solely upon the selection of materials and their method of fabrication to minimize the possible air spaces that could occur between the conductor and insulation (vicinity of maximum stress under an a-c potential), at voids within the dielectric material itself, or between the insulation and any outer coverings or medium. Therefore, one of the main objectives of this investigation was to study ways and means of properly evaluating this characteristic of rubber and extruded insulated cables and the subsequent ways and means of improvement.

The deleterious effects of ionization are well known and have been rigorously studied by other investigators. As early as 1893 Steinmetz[1] presented a paper on the subject followed by many others.[2–6] In spite of limited measuring equipment available to the earliest investigators these men established the basic principles of field theory, corona formation, and made some good predictions as to the probable nature of the phenomenon of ionization. The art of making cables was far enough advanced so that the application of higher voltages in cables paralleled the use of higher voltage on overhead transmission lines. It is not surprising therefore that in 1898, R. F. Fessenden performed experiments which showed the danger of air bubbles in solid insulation. It was soon discovered that ionization of air spaces within solid dielectrics caused a deterioration of organic insulation.

If the electrical stress is sufficient to cause ionization in voids or air spaces, failure of the insulation may occur without excessive general temperature rise through localized overheating due to increased power loss in the void, or to progressive local deterioration due to discharges. Ionization of air causes the formation of small quantities of ozone and oxides of nitrogen. The nitrogen oxides may chemically attack cellulose materials and certain metals whereas the presence of ozone leads to rapid physical deterioration of insulating materials and in the case of nonozone-resistant rubber insulations, causes corona cuts or checking similar in appearance to that produced by strong sunlight. Because a wide band of frequencies is produced by corona discharge, serious radio or television interference may be caused by ionization, either by direct radiation or by conduction through the circuit itself. Thus, it is important to the manufacturer as well as the user to know the effects of and to be able to determine the point at which ionization originates in any circuit component.

Until 1930 the measurement of ionization point was made indirectly by means of its effect on the dielectric loss or power factor of insulation. This was measured at first by special wattmeters and later by high-voltage power-factor bridges. Since high-frequency disturbances are associated with ionization and since the progress of electronics led to the development of sensitive amplifiers and detec-

* Ionization is a fundamental process which is found in various forms in many branches of electrical engineering. The word is used here to describe that ionization which may occur in a dielectric subjected to electrical stress. More specifically, the expression "ionization point" is used herein as a synonym for other commonly used but similar meaning terms such as "corona or corona-ionization level," "corona extinction or ignition voltage," and "critical ionization potential." When this expression is used in the text, reference will be made to extinction voltage unless otherwise explained.

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R. C. Graham and E. K. Duffy are with the Rome Cable Corporation, Rome, N. Y., and W. P. Foster is with the Thermionic Engineering Corporation, Pompton Plains, N. J.

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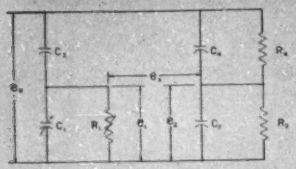

Figure 1. Ionization bridge circuit

tors, the next step was to use these disturbances as a measure of ionization point. This was done by several investigators between 1930 and 1945.[7–11] However, most of the equipment developed to date was individually designed and constructed for laboratory purposes only. These apparatus varied in degree of sensitivity and accuracy, required special sample preparation, and were limited to measurement of relatively small or low capacity specimens. This presented the need for development of a satisfactory commercial instrument with a high degree of accuracy and sensitivity which would be safely and simply used for regular production measurements as well as in the laboratory and in the field.

New Concepts in the Measurement of Ionization

It is generally believed that ionization is the result of bombardment of molecules by particles of atomic or subatomic dimensions. It is believed that the bombarding particles are accelerated by the application of high-voltage gradient. This fundamental bombardment concept has been shown and used repeatedly in the study of atomic physics. Ionization results when the bombardment causes electrons to become freed from their parent molecule. Regardless of the exact mechanism of release, each freed electron induces a charge on the electrodes near it. The magnitude of the charge is 1.6×10^{-19} coulombs in the rationalized meter-kilogram-second system of units.

In a capacitor or short length of cable, the release of electrons will charge the plates or conductors in a very short time to a voltage which is determined from

$$V=\frac{Q}{C} \tag{1}$$

In the equation, Q is the charge in coulombs which is equivalent to the number of electrons released, multiplied by the charge on each electron as given before. The capacitance C is that of the capacitor or cable. An assumption is made that the plates of the capacitor or cable are isolated by an inductance from the circuit which completes a path from positive to negative plate. If this is so, the capacitor becomes completely charged before an appreciable charge leaks off through the external circuit. Even up to a length of several thousand feet, the time required for charging wavefront to traverse a cable is only a few microseconds. A cable connected through an inductance to an external circuit and charged instantaneously by release of electrons behaves from that point on as though it had received a step voltage. The response in the network can be determined by transient analysis.

The use of a bridge circuit for balancing external disturbances out of the ionization detector is well known. It is also known that complete balance is never obtained. This latter is attributed to the fact that all known high voltage bridges give a satisfactory balance only over a band of frequencies which is narrow compared to the bandwidth covered by ionization voltages. In order to understand the limitations of this circuit, it is desirable to analyze the response of a bridge to signals of the type previously described.

A Schering bridge, balanced at one frequency, is balanced at all frequencies, if the standard and the unknown capacitors both have the same power factor. This is equivalent, in the terminology of transient analysis, to saying that both the unknown and the standard have the same time constant if the bridge balances to an impulse or step signal. This equality between time constants is a necessary condition for bridge balance when the applied signal contains impulses, rectangular pulses, or step voltages having a steep front. At first glance, this would seem to rule out the bridge as far as eliminating external transients is concerned, since the unknown will, in general, have a different time constant than the standard. However, a transient study of the circuit provides information which leads to a more efficient use of the bridge circuit.

In Figure 1, a circuit is shown which has a different power factor or time constant in the standard (C_3) from that of the unknown (C_4). To solve this circuit most expeditiously, assume that an infinite impedance detector will be used. It is then permissible to consider each branch of the bridge separately and compare the voltages across C_1 and C_3. Consider the left hand branch of the bridge with a step voltage applied and solve for the voltage across C_1.

$$e_1=e_0\,\frac{C_3}{C_1+C_3}\;\epsilon^{\frac{-t}{R_1(C_1+C_3)}} \tag{2}$$

In equation 2, e_1 represents the voltage across C_1, e_0 represents a step voltage of amplitude e_0, t represents time after the application of e_0 and the remaining letters represent the circuit constants. The value of e_1 plotted against time for a representative bridge is shown on Figure 2.

Next consider the right hand side of the bridge with the same step voltage applied. In this case, there will be a steady-state response which can be ignored because the measuring network and the detector will greatly attenuate any d-c signal.

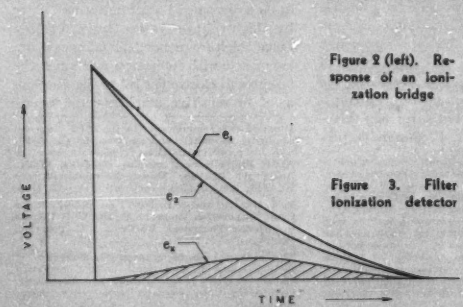

Figure 2 (left). Response of an ionization bridge

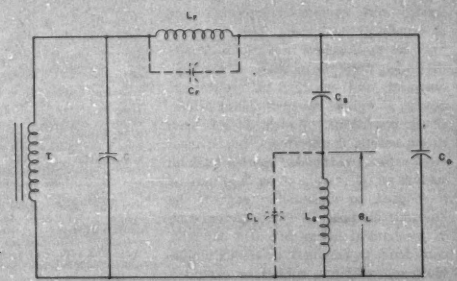

Figure 3. Filter ionization detector

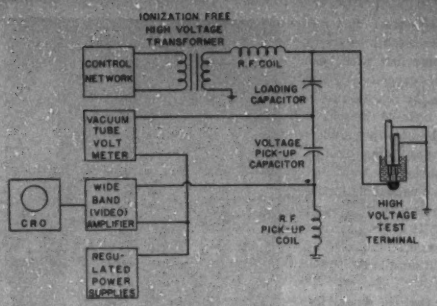

Figure 4 (left). Basic circuit of self-contained ionization measuring equipment

Figure 5. Recently developed ionization detector (front view)

The transient response is seen as follows.

$$e_3 = e_0 \frac{C_4}{C_3+C_4} \, \epsilon^{\frac{-t}{R_3(C_3+C_4)}} \times \left[1-\left(\frac{C_3+C_4}{C_4}\right)\left(\frac{R_4}{R_3+R_4}\right)\right] \epsilon^{\frac{-t}{R_4(C_3+C_4)}} \quad (3)$$

In equation 3, e_3 represents the voltage across C_3. It will be seen that this is given by the product of two exponentials, one of which is similar in form to equation 2. This first exponential can be made exactly the same as equation 2 by maintaining the following equalities.

$$\frac{C_1}{C_3} = \frac{C_2}{C_4} \quad (4)$$

$$R_1C_1 = R_3C_3 \quad (5)$$

If this is done

$$e_3 = e_1 \left[1-\left(\frac{C_3+C_4}{C_4}\right)\left(\frac{R_3}{R_3+R_4}\right)\right] \times \epsilon^{\frac{-t}{R_4(C_3+C_4)}} \quad (6)$$

Formally, in such a bridge, the quantity $(C_3+C_4)/C_4$ will not exceed 100 while the quantity R_3/R_3+R_4 will easily be less than 0.0001. Equation 6 may thus be simplified giving as a close approximation

$$e_3 = e_1 \, \epsilon^{\frac{-t}{R_4(C_3+C_4)}} \quad (7)$$

It will be found that e_3 as given in equation 7 is equal to e_1 (equation 2) where $t=0$ and $t=\infty$. Both voltages are shown in Figure 2 and also their difference e_x.

From equations 4 and 5 it is apparent that the balance which results in the voltage e_x is obtained by making the capacitance balance normally as in conventional bridges. The time constant balance is made by making the time constant of C_1 equal that of C_3 regardless of the difference between time constants in standard and unknown (C_3 and C_4). It has been assumed that the standard has nearly zero power factor and power factor of the unknown is not greater than 20 per cent.

The response of the bridge to pulses originating within the bridge is more difficult to compute algebraically and was abandoned in favor of numerical analysis of a suitable bridge designed on the basis of the previous equations. The numerical analysis showed that the response to a step voltage applied in series with the unknown capacitor is a steep front exponential like e_1 and e_2 of Figure 2. Its peak amplitude is about 30 times as great as that obtained when the same step voltage is applied externally to the balanced bridge. Furthermore, the response to external signal is much flatter. For a fairly close graphical comparison of the responses to the same signal applied in the two different places, see e_1 and e_x of Figure 2.

In the actual circuits if there is ionization external to the bridge it will generally be of much greater magnitude than the first stages of ionization within the bridge. The 30-to-1 advantage described before is easily overbalanced by excessive ionization external to the bridge. It is possible to make use of the flatness of the response to external ionization in further discriminating between it and the desired response. If the signal is passed through an electronic differentiating amplifier or, RC peaker, there will be an attenuation of flat pulses with respect to steep pulses. By using a bridge circuit designed according to the principles discussed before and using a differentiating detector, it is expected that rejection ratios of several 1,000 to 1 may be obtained.

Within the last 12 years, there has appeared a different form of ionization detection circuit. This circuit uses a filter to discriminate between ionization signals originating within the unknown capacitor and those originating externally. Early models were built so that the sheath of the unknown capacitor or cable was required to be insulated from ground. This is not an inherent requirement. Recent equipment permits grounding the sample. This feature, combined with the absence of any balancing procedure, is sufficient cause for interest in the circuit.

The simplest form of the filter circuit is shown in Figure 3. The secondary of the high voltage transformer is represented by its distributed capacitance, C_1. The capacitance C_3 represents the sample. A loading capacitor C_r is also supplied and in series with it is the pick up coil L_s with its distributed capacity C_L. An r-f reactor L_F is connected in the high voltage lead

Figure 6. Recently developed ionization detector (rear view)

Figure 7. Ionization detector (front view)

This instrument used for production and laboratory tests described in this paper

between the transformer and the loading capacitor. The distributed capacity of this coil C_F is important. The response in L_e to a transient applied at either C_1 or C_0 depends on the ratio of the capacities in the circuit during the initial instant. If C_F is large compared with the other capacities in the circuit, there will be coupling between the two meshes regardless of the value of inductor L_F.

The ratio of the responses at L_e to equal step voltages applied at C_1 and C_0 is easily computed using the ratio of capacitances and the circuit of Figure 3. It is

$$\frac{e_{L1}}{e_{L0}} = C_0\left(\frac{1}{C_1}+\frac{1}{C_F}\right) \tag{8}$$

where e_{L1} represents the response to external signal applied at C_1 and e_{L0} represents response to the ionization signal in the unknown capacitor C_0. It will be seen that the rejection ratio depends on the size of the unknown capacitor and is more favorable as the capacity increases.

The capacitance of a 10-foot length of typical shielded cable is about 600 micromicrofarads. If C_F is made as small as 20, and C_1 equal to 200 micromicrofarads, the available rejection ratio is 33. This is satisfactory only when external disturbances and any appreciable ionization are eliminated in the high voltage transformer or other circuit components. Where such ionization is present, a rejection ratio of 10,000 or more would be desirable. This could be obtained if the length of cable tested was in the order of a few thousand feet. To test such a length of cable requires a transformer with adequate kilovolt-ampere capacity. Other methods of increasing the rejection ratio have been tried, but none of them have been successful enough to warrant further consideration. The outstanding result of this analysis is to show that the high voltage supply and other components in the filter-type ionization-detection circuits must be corona free. It is obvious that the loading capacitor and all connections to the transformer and sample must also be corona free.

It is suggested here that the sensitivity of ionization detectors of the electronic type might be defined as the double amplitude of a square wave applied to the specimen terminals which will produce a pulse 1-inch high on the screen of the oscilloscope used as an indicator. A method of applying this calibrating square wave while the set is operating follows. Connect a capacitor of approximately 0.002 microfarad to the high-voltage test terminal. This capacitor should be corona free to at least 5-kv. In series with it connect a capacitor of at least 2 microfarads. Apply the square wave across this latter. From the amplitude of the square wave, the ratio between the two capacitors and the internal circuit of the detector it is possible to compute the square wave amplitude at the test terminal.

Figure 8. Ionization detector (rear view)

Figure 9. High voltage terminal

Shows cable sample under test

Modern Methods and Equipment Used for Detection of Ionization in the Range 0–35 Kv

Schering Bridge Circuits

Most existing ionization detectors use the Schering bridge circuit. This alone is not capable of balancing out all extraneous impulses, except in the few cases where the standard and the sample capacitors are nearly identical in capacity and power factor. For this reason, experimenters working with a wide range of sample sizes have had to make their high voltage supply comparatively free from ionization.

A further precaution taken by some[12] is that of using a variable standard capacitor, a feature which is expensive to build and not easy to arrange for safe operation. The size of such a capacitor is limited, which, in turn limits the size and length of cable that can be tested. Other disadvantages of the bridge method are the need for balancing each sample, and the fact that the sample sheath cannot be at ground potential, such as would be required for production tests in grounded water tanks.

However, for laboratory setups on small samples, the bridge method is probably as good as any other. Where existing transformers must be used or where, for other reasons, some ionization external to the bridge is expected, the bridge is the only method that can be used. Where a bridge is used, the analysis of bridge per-

Figure 10 (left). Probe electrode assembly

Shows cable under test

Figure 11. Removable probe electrodes of various sizes

formance as described previously will be a useful guide.

Because of the lack of inductors in the bridge circuit ionization appears as a unidirectional pulse if a broad-band amplifier is used. Ionization in the standard capacitor will then appear in the opposite direction from that in the sample. This fact has been used to discriminate between ionization in the body of the sample and ionization in the terminations. A guard ring on each end of the sample was connected to the return of the standard. Thus any ionization in the guard circuit was indicated as part of the standard ionization. Another advantage of the bridge circuit lies in its simplicity from a computation point of view. This gives an advantage in basic measurements of ionization. The bridge circuit can be set up for operation at a point remote from the high-voltage area. This is only true when the standard capacitor is fixed. If so, it offers a strong argument from a safety point of view. This, however, is more important where test voltages are greater than 35-kv, in which case the use of the bridge circuit is universal.

Filter Circuits—Development of Commercial Equipment

Until quite recently, manufacturers and other investigators interested in ionization testing have had to design and construct their own equipment. This gave rise to a lack of uniformity, standard procedures, and results which have limited any widespread practical applications. This experience has indicated that a need existed for certain definite ionization instruments for laboratory, field, and especially production measurements.

It was decided that the most desirable arrangement for commercial equipment for cable and similar component testing should involve the filter circuit since it is the easiest to operate and maintain. Moreover, it can be used to measure an extremely wide range of capacitance without requirement of a balancing operation and permits the cable sheath to be connected to ground. Cables without conducting sheaths or shields can be tested in the same grounded water tanks used in all cable factories to perform the conventional voltage and insulation resistance

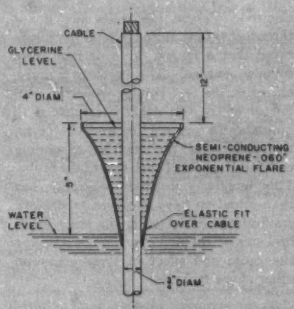

Figure 12. Ionization-free terminal

tests on rubber insulated conductors. Since the testing unit itself could be compactly designed to be semiportable and entirely self-contained, the high voltage supply and associated components must be free of ionization and, therefore, the rejection ratio need not be high. It has been found that testing with such equipment can be as rapid and safe as making dielectric tests with a portable dielectric tester. A circuit diagram showing the essential parts of these ionization detectors is given in Figure 4.

Figures 5 and 6 show the front and rear view of a recently developed ionization tester based upon these conditions. This portable equipment was designed mainly for general laboratory applications and is complete within itself. The unit is corona free to 30,000 volts and contains a 5-kva 60-cycle high-voltage transformer. The loading capacitor is an oil-filled paper type such as used for surge generators and is rated at 70,000 volts at 60 cycles continuously. This capacitor has a value of 0.003 microfarad. Voltage is measured directly using a capacitor divider and a vacuum tube voltmeter. An induction regulator is used to control the voltage. This model includes a broadband amplifier which shows up the pulse nature of ionization quite clearly. Since there seems to be good reason for considering ionization as a step voltage it seemed logical to calibrate the amplifier in terms of a similar signal. The sensitivity of this instrument measured by the previously proposed method is 0.1 volt. This was found to be more than ample.

Figures 7 and 8 show the front and rear view of an ionization test instrument specially designed for production tests on rubber insulated and other extruded cable types and for general purpose laboratory tests on cable samples and components. This is the instrument which was used to obtain all of the measurements to be described later. In most respects it is similar to the unit previously described but is corona free to a rated maximum of 15,000 volts and a higher capacity of 10-kva in

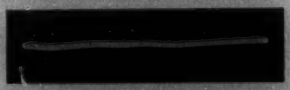

Figure 13. Oscilloscope trace at voltage below ionization point

Figure 14. Oscilloscope trace indicating ionization point

Table I. Ionization Detector Versus 60-Cycle Schering Bridge

Cable Sample 350,000 Circular Mils, Lead Sheath

Cable Length	Ionization Point in Kilovolts	
	Schering Bridge	Detector
250-foot reel	6.0	5.8
15-foot sample from above reel	6.0	6.0
15-foot sample from above reel	6.0	5.8

order to provide the required charging current for long or full factory reel lengths of cable. The loading capacitor is rated at 33,000 volts and has a capacitance of 0.005 microfarad. The pick-up coil is a 25-millihenry inductor. Voltage regulation is by means of an autotransformer and voltage measurement is accomplished by a primary voltmeter. The controls, including a foot-operated switch, are so arranged that it is impossible to energize the high-voltage transformer until the regulating autotransformer has been returned to zero. This prevents the accidental and sudden application of full voltage to the sample or cable under test. At the rear of the unit, in a protected location, is an oil-filled bakelite tube which is used as a high-voltage terminal (Figure 9). A mercury pool located in the electrode at the base of the tube provides means of making spark-free contact. This is the conventional method of connecting samples to this type of ionization detector and does not require more than ordinary care in the preparation of samples. It is adaptable to a wide range of cable sizes and constructions and can be readily disconnected.

It is possible with additional external terminal equipment to test short sections of a long cable length without cutting the piece. In order to do this a probe electrode assembly was designed as shown in Figure 10 with different removable probe electrodes as shown in Figure 11. The cable to be tested must be unshielded and is passed through the close fitting tubular

Table II. Oil Terminal Versus Glycerine Terminal and Probe

Cable Sample Number 4-0 AWG Stranded, 10-Foot Lead-Foil Electrode

Method of Termination	Ionization Point in Kilovolts
Oil terminal	8.0
Glycerine terminal	8.2
No terminal	2.2
Probe readings at 2 foot spacings with lead-foil removed	8.0, 8.7, 8.4, 8.0, 8.6

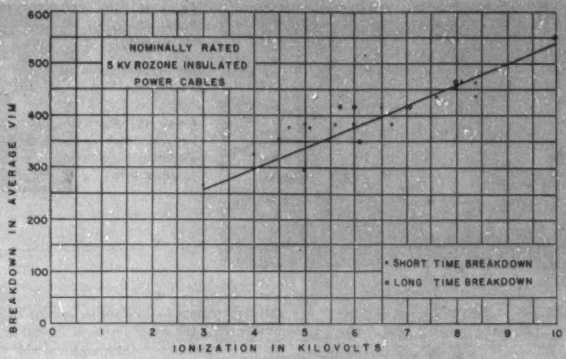

Figure 15. Relationship of breakdown voltage versus ionization

Represents data obtained on RoZone insulated conductors, number 8 AWG stranded through 500,000 circular mils, stranded

electrode. These electrodes are about three inches long although tests have indicated that a longer electrode may have some advantage. The electrode is maintained at the test voltage while the conductor of the test cable is connected to ground. To prevent induced charge on the surface of the sample, grounding rollers are placed at each side of the probe. This auxiliary electrode assembly has been found to have excellent laboratory applications in detecting the exact localized area of low ionization and in studying the cause and effect of these areas on other characteristics.

Indicators

The indicators used for ionization are varied. Early equipment took advantage of the sensitivity of head phones and ionization was identified by its peculiar frying sound. Later, amplifiers were used in conjunction with a rectifier voltmeter or a peak reading voltmeter which gave a good indication and offered a better measure of ionization intensity. Since the initial signal is a steep wavefront and the response depends on the nature of the circuit as well as the characteristics of the indicator, it is difficult to find any means of calibrating two differently designed instruments so that their results can be correlated. The best solution has been to use an oscilloscope which provides a measure of the peak amplitude of pulses. It shows the wave shape so that different kinds of response can be separated and it can show the relative position on the exciting wave at which the pulse is formed. It combines all the good features of the indicators previously listed and has none of the bad features. The only thing lacking in an oscilloscope indicator is a means of integrating or counting pulses, but at the present stage of ionization measurement an integrating device is not essential since the objective is to find the ionization ignition and extinction voltages.

For certain laboratory applications it may be desirable to add a pulse counter to the oscilloscope in order to obtain an indication of the total quantity of ionization. For some applications a recorder has been used.[13]

Investigation of Ionization Level of Rubber Insulated Cables

The evolution of the testing of solid dielectrics has been a history of improvement of electrical apparatus and methods of analysis. Through this evolutionary method a great number of valuable and informative tests have been developed for the evaluation of the elements of insulated cable, and to establish the critical limits of operation. These include some full-reel length tests such as insulation resistance, capacitance, power factor, and voltage test; and additional short length tests for specific insulations such as rubber, to evaluate the dielectric material itself with respect to dielectric strength, moisture resistance, ozone resistance, and physical properties. These tests do not entirely evaluate a cable made of these elements.

Within the structure it is possible that at some point ionizable voids (air

Table III. Long-Time Voltage Application Versus Ionization Point

Cable Sample Nominally Rated 5 Kv

Conductor Size	Ionization Point in Kilovolts: Initial	Ionization Point in Kilovolts: After 1 Week at 200 Volts per Mil
Number 4/0 AWG, stranded	9.2	8.0
Number 4/0 AWG, stranded	9.2	10.0
350,000 circular mils, stranded	6.1	7.1
	5.1	5.7
	5.8	6.0
	5.0	6.1

spaces) may exist between the strand and insulation, within the insulation or between the insulation and shield or sheath. These defects may be due to manufacturing errors or to displacement after manufacture because of flexing or other mechanical distortion.

During service, ionization may take place with extremely destructive effects. The result is attributed to corona cutting, oxidation, weak spots in the insulation or just low dielectric strength; since the evidence necessary for a critical analysis is generally destroyed, or the mechanism of breakdown has created evidence common to any of these causes.

Such tests as have been developed for the detection of these types of discontinuity or nonhomogeneity in the insulation are destructive and therefore confined to the short lengths mentioned before. There appears to be a need for a method to detect such voids by nondestructive means which may be applied to any length of cable, with or without a grounded sheath. The ionization detecting equipment previously described has been developed to fulfill this need, and was used to obtain the information given in the following section.

Cable Preparation and Measurement

In order to accurately determine the voltage at which ionization occurs within insulated cables, the method of terminating the cable is of prime importance. The operator must be assured that the cable ends are free of surface leakage in the form of discharges since they are readily picked up through the detector circuit at voltages considerably lower than the ionization under measurement. The method of termination should be readily prepared and simple in form, especially if it is to be adapted to production testing where full-reel lengths are immersed in test tanks or shielded with some form of conductive sheath. This section describes the development and subsequent use of two distinct types of terminations that have been proven successful for use in laboratory and production testing, in that they are simple, convenient to use and corona-free at the test voltages encountered.

Figure 9 shows the first method of termination especially adapted to shielded cables, which consists of an oil-filled bakelite tube with a mercury cup at the bottom for the high-voltage connection. The cable ends are prepared by removing the conductive shield for a distance of a foot, forming a smooth rounded surface at the edge, and then immersing them in the oil-filled terminal so that the conductors come in contact with the mercury electrode. Unshielded cables immersed in test tanks may also be tested using this termination, by simply sliding a snug-fitting copper shielding braid over the exposed ends and treating it as a shielded cable. This type of terminal has been found to be corona-free up to 35 kv and is constructed as an integral part of the measuring equipment.

Figure 12 shows the second method of termination especially adapted to production testing of unshielded finished cables and insulated conductors immersed in test tanks. The premolded, semiconducting Neoprene terminal has an exponential flare which eliminates concentrated stress at the water line, and is filled with a high-dielectric constant fluid such as glycerine which absorbs any formation of corona energy due to end leakage. The flexibility and elasticity of the terminal permits a snug fit around the cable insulation which holds it in proper position and prevents leakage of the glycerine. Terminals can be made up in several sizes to accommodate the cables under test. Shielded cables may also be tested using this method by terminating the conductive sheath at the base of the cone, and applying tightly wrapped metallic foil over the end of the conductive sheath extending over the end of the preformed cone. Check measurements made with the Schering bridge show that this method is satisfactory up to 15 kv and possibly higher, which will be determined upon further investigation.

Figure 16. X-Ray photograph indicating loose insulation

Once the selected terminal is applied, the operator only has to raise the test voltage until the first signs of high-frequency oscillations appear on the oscilloscope trace (Figures 13 and 14) indicating the inception point. The voltage is then gradually decreased until all signs of corona disappear, which is the extinction point and occurs at a voltage about ten per cent lower due to the reduced energy necessary to maintain the ionization occurring within the cable. Ionization values given in this paper represent the extinction voltage, since cables stressed at this level in operation would only require a slight surge to initiate the ionization. The information presented in this section was obtained on nominally rated 5-kv oil-base rubber insulated cables conforming to American Society for Testing Materials specification *D574* and will be referred to hereafter by conductor size and construction.

Table IV. Ionization Point Versus Temperature

Cable Sample A 350,000 circular mils lead sheath (no conductor adhesive)
B Number 4-0 AWG, Stranded, lead sheath (with conductor adhesive)

Cable	Ionization Point in Kilovolts: 20°C	40°C	60°C	80°C	100°C	100°C*	80°C	60°C	40°C	20°C
A	6.8	6.7	6.3	5.0	5.1	5.5	5.6	6.1	6.3	6.8
B	5.2	5.0	4.9	5.0	4.8	5.0	5.0	5.0	5.0	5.0

*Maintained at 100 degrees centigrade for 16 hours.

Table V. Ionization Level for Patching Analysis

Cable	Ionization Point in Kilovolts: Cable	Ionization Point in Kilovolts: Patch	Breakdown Voltage: Kilovolts	Breakdown Voltage: Volts per Mil
350,000 circular mils, stranded	5.2	5.3	65	346
	5.5	5.5	65	346
	5.1	5.2	65	346
	5.2	5.4	65	346
Number 6 AWG stranded	8.9	4.3	25	133
	7.9	4.9	30	160
	9.9	4.8	26	133

Table VII. Conductor Adhesives for Cables Rated at 5 Kv

Cable Sample C: 2/C Number 8 AWG Solid

Reel Number	Condition of Manufacture	Ionization Level in Kilovolts
C1	With conductor adhesive	6.0
C2	Without conductor adhesive	3.0

Ionization Detector versus Power Factor Bridge

Sufficient data have been obtained on various cable sizes and lengths ranging from 2,000-foot reels to 15-foot samples, whereby the accuracy of the ionization detector using the terminations developed was checked against the standard 60-cycle Schering bridge method as shown in Tables I and II. The fact that the detector directly measures the lowest point of ionization level of the cable under test, as against the Schering bridge method which approaches an over-all ionization level, is of great importance since the low value is associated with the weakest part of the cable. The latter statement was verified by the measurement of two 15-foot number 4/0 American wire gauge (AWG) stranded cables whose ionization levels were 3.5 and 6.0 kv as obtained by the Schering bridge method. The two cables were then measured simultaneously, the resulting ionization level being 5.4 kv. This is accounted for by the limited sensitivity of the Schering bridge in measuring very small over-all power factor increases due to ionization of localized air voids or spaces.

Breakdown Voltage Versus Ionization Point

It has been the general opinion that internal ionization level of rubber insulated power cables bears some relation to the breakdown voltage since voids, porous insulation or loose insulations are detrimental factors, as explained previously and could influence both values in the same manner. As a starting point in this investigation cable samples were limited to those nominally rated at 5 kv, shielded and unshielded, with as complete a coverage of conductor sizes as was possible. The ionization level was measured on each sample prior to breakdown, which was conducted on 15-foot lengths, properly terminated, by applying 200 average volts per mil for five minutes and then increasing the voltage in 5-kv steps at 5-minute intervals. Six of the samples were subjected to 200 volts per mil for a week to determine what effects this would have on the ionization level and corresponding breakdown.

The data presented in this section, although preliminary inform in that they only represent 5-kv wall thicknesses, indicate a definite pattern as shown in Figure 15, from which a relationship between ionization level and breakdown voltage could be obtained. However, no attempt will be made to expand this relationship at the present time, since it is felt that additional data now being compiled with respect to various wall thicknesses will be more substantiating. As can be seen in Figure 15 and Table III, the cables subjected to 200 volts per mil (average) for a week showed no appreciable change in ionization level or breakdown voltage. Additional data with respect to longer periods of voltage application are also under way.

Table VI. Conductor Adhesives for Cables Rated at 5 Kilovolts

Cable Sample A: Full reel length of 350,000 circular mils, stranded, manufactured without conductor adhesive

Cable Sample B: Full reel length of number 4/0 AWG stranded, manufactured with conductor adhesive

Reel Number	Ionization Level in Kilovolts: Before Loading	Ionization Level in Kilovolts: After Loading
A1	5.2	3.5
A2	5.5	3.3
A3	5.2	3.3
A4	5.0	3.0
B1	5.4	5.4

Ionization Point Versus Temperature

The question arises as to whether or not the measurement of the ionization level of a cable at room temperature is indicative of its operation under load, inasmuch as the operating temperatures are usually in the order of 75 degrees centigrade. Table IV indicates no appreciable change in ionization level for a cable using conductor adhesives, and a drop from 6.8 kv to 5.0 kv for a cable without adhesive. Since some high-voltage cables are manufactured with conductor adhesives, the measurement of ionization level at room temperature on such cables is considered justifiable. It is further significant that the ionization level at room temperature should exceed the operating voltage if any safety factor is desired at the operating temperature.

Probe Measurements

As was mentioned previously, it is possible to confine the ionization measurements to short sections of a long unshielded cable, through the use of a close fitting tubular probe about three inches in length as shown in Figures 10 and 11. This is a significant development in view of the fact that it provides a nondestructive means of locating voids or air spaces within a long cable length which can then be analyzed by dissection or X ray to determine its exact nature. The information thus gained can be used in producing improved cable.

Data were obtained on a full reel of number 8 AWG stranded cable showing its ionization point to be 3 kv, which indicated poor cable construction somewhere within the 1000-foot length, since similar reels tested at the same time had an ionization level of 10 kv. By passing the cable through the probe with the applied voltage set at 7.5 kv, no ionization was recorded until a section of cable was located at which point the ionization level was 3.0 kv. X-ray photographs and visual examination indicated that the conductor adhesive was lacking and the insulation loose at this point as shown in Figure 16. Since this could not be detected with the standard nondestructive methods it establishes the importance of this analysis.

Use of Ionization Detector for Analysis of Factory Repairs in Cables

Besides being able to locate low ionization areas in long cable lengths, the probe apparatus proved advantageous in analyzing cable repairs. By passing the repaired cable section through the probe and measuring the ionization of the insulation patch and the cable on either side, it is possible to locate void forma-

Table VIII. Typical Factory Test Results on Nominally Rated 5-Kilovolt Cables

Size	Description	Length Feet	Ionization Level Kilovolts
350,000 circular mils, stranded	Unshielded	1060	5.2
		1030	5.5
		1030	5.3
		1030	5.0
Number 4/0 AWG, stranded	Neoprene sheathed	1000	9.3
350,000 circular mils, stranded	Lead sheath	1020	5.6
Number 2/0 AWG, stranded	Lead sheath	916	5.8
250,000 circular mils, stranded	Unshielded	1040	6.0
Number 8 AWG, solid	Unshielded	2200	6.0
350,000 circular mils, stranded	Unshielded	1025	9.4*

*Rejected due to loose insulation.

tions that might have occurred during the taping or molding operation. Table V illustrates this, in that the patches whose ionization level was equal to that of the cable were satisfactory, as indicated by breakdown voltage and visual analysis. The patches whose ionization was below that of the cable were not satisfactory due to void formations.

Such a method of evaluating patches lends itself readily to production testing, since it is nondestructive and serves as an indicator of acceptability. The probe electrode could be of the split-type so that it would not be necessary to pass the entire cable length through the probe. A similar method of analysis using properly designed portable detection equipment might well prove advantageous in application to field splices and repairs.

Use of Conductor Adhesives for Rubber Insulated Power Cables

Use of conductor adhesives has been suggested as a means of maintaining a high ionization level for rubber insulated cables with stranded conductors. Insulating rubber cements applied over the conductor prior to the insulation fills the strand interstices and, in addition, insures a strong bond which prevents loosening of the insulation due to flexing when undergoing further processing or handling during installation. An investigation was conducted whereby full-reel lengths of 350,000 circular mils cables were manufactured without any conductor adhesives and measured with the detector while immersed in test tanks prior to the application of a lead sheath. The respective ionization levels were measured again after the lead sheath had been applied. The results of this test shown in Table VI definitely indicate a drop from the initial 5-kv ionization point to the final 3-kv ionization point which is believed due to the further process handling combined with the lack of conductor adhesive. On the other hand, an accompanying reel of number 4/0 AWG stranded cable manufactured with conductor adhesive showed no decrease in ionization level.

Further data showing the need for conductor adhesives were obtained on two reels of two conductor number 8 AWG solid cable, where one reel of cable was manufactured with conductor adhesive and the other without. Table VII shows the ionization level of each reel to be 6 kv and 3 kv respectively.

The value of conductor adhesives becomes more apparent with respect to ionization at the elevated operating temperatures of the cable, where the expansion characteristics tend to loosen the insulation from the conductors as shown in Table IV.

Conductor Shielding for Rubber Insulated Power Cables

Use of conductor shielding in the form of semiconducting tapes or compounds is another possible means of maintaining a high ionization level for rubber insulated cables and is successfully being incorporated in the manufacture of cables at the present time. A sample of number 4/0 AWG stranded cable which had a semiconducting cloth tape applied over the strand prior to insulating showed improved dielectric strength in the order of 37 per cent and an ionization level which was increased approximately 1.5 kv when compared with conventional conductor adhesive. This is significant in that it indicates the possibility of similar beneficial effects as obtained with the use of conductor adhesives, besides permitting free stripping qualities of the insulation. A further study of the use of conductor shielding is in order for cables wherein free stripping properties of the insulation can be tolerated.

Production Tests on Full Reel Lengths

Typical factory test results of ionization level obtained on full-reel lengths of nominally rated 5-kv cables using the ionization detector are shown in Table VIII. This not only indicates the possible use of the detector for production testing, but also shows that cables can be manufactured so that their ionization level is above their nominal rating. Its use is advantageous also in that low-ionization levels usually indicate faulty cable construction which probably would not be detected under standard factory test procedures, especially if the bad section of cable were well within the reel length.

All unshielded cables including Neoprene-sheathed type were tested in water tanks. All lead sheathed cables were tested with the sheath grounded.

While all of the foregoing investigation was confined to rubber insulated cables, considerable data have also been obtained upon polyethylene thermoplastic insulated types of power and communication cables. The expansion and contraction characteristics of polyethylene are so appreciable that very special extrusion and handling conditions must be observed if a void-free product is to be obtained. Since the ionization of voids in polyethylene cause rapidly deteriorating effects[14] it is already recognized that the ionization detector must be used as a production control test on any polyethylene cable intended to operate at voltages in excess of 1,000 if satisfactory performance is to be obtained. Investigations relating to this subject are being continued and will be reported at a future date.

Inasmuch as it has been shown that the ionization detector can be used as a practical nondestructive production or laboratory test on rubber insulated cables, and since the effects of ionization are deleterious, it would seem that such a test would be advantageous to the manufacturer as well as the cable user. The significance of ionization tests is not limited in its importance to cables. The very fact that the transformers, capacitors, connectors, and other circuit components used in the ionization detector unit itself required special treatments and, in some cases, redesign in order to obtain corona-free performance within their nominally rated voltage range, indicates a similar need for rigid investigation.

Conclusions

1. New concepts of equipment design together with the development of commercial testing units has been described, and the value of the ionization detector as a nondestructive test for cables and cable repairs is analyzed.

2. A relationship between ionization level and dielectric strength has been shown to

exist for a definite insulation and cable construction. Methods of overcoming the deleterious effects of ionization have been discussed indicating their advantages.

3. Corona-free terminal designs have been developed and adapted to production tests of ionization level on full-reel lengths of insulated cables.

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Discussion

Theodore F. Stipp (Department of Water and Power, Los Angeles, Calif.): It appears that the authors have developed testing equipment and methods that should prove extremely useful, both in production testing and in improving cable design and manufacturing methods.

We have never felt that the common commercial method of testing for ionization was of much use in determining the presence of voids in rubber or synthetic insulations; so in purchasing cable of that type, we have not asked for ionization tests on our 5-kv cable, and have specified that our 34.5-kv cable be made with a semiconducting tape over the conductor and another semiconducting tape over the insulation, which, we note that the authors state, materially increases the dielectric strength and ionization level.

The statement of the authors as to the need of investigation into ionization and corona in transformers and other equipment was proved to us in 1941 when our test laboratories, using connections as given G. E. Quinn[1] and some modifications, ran some tests in an effort to determine the voltage level at which bushings and insulators would give radio interference. Due to interference caused by the high-voltage transformer, it was possible to run tests only on small pin-type insulators up to 10 kv, so these tests were not carried any further.

Reference

1. A Method for Detecting the Ionization Point on Electrical Apparatus, G. E. Quinn. AIEE Transactions, volume 59, December 1940, pages 680–2 and 1232.

C. E. Betzer (Commonwealth Edison Company, Chicago, Ill.): The authors show that with modern methods of manufacture 5,000-volt rubber-insulated cables can be made free from corona at rated voltage. That is an improvement as such cables made ten years ago had lower corona starting voltages. At that time, tape and saturated cotton braid were commonly used over the insulation and much of the cable was unshielded. In tests at the University of Illinois of samples resting on metal plates, the corona-starting voltages were generally from 2,400 to 3,000 volts between conductor and plate. Those same cables with the braids removed had corona-starting voltages from 3,000 to 4,400 volts. Now the use of braids has been generally discontinued and jackets of synthetic rubber are used for outer coverings. By the use of adhesive or conducting rubber between the insulation and conductor and tight shielding or conducting rubber over the insulation, corona can be eliminated.

The application of lead sheath over rubber insulation is shown to decrease markedly the ionization level, in some cases. This suggests that handling and aging might have a similar effect. The tests at the University of Illinois included some tests of cables that had been bent, some that had been stored for two years, and some that had been in service for from five to seven years. Such handling and aging was found to produce no consistent change in the ionization level.

Some of the rubber compounds have been able to withstand moderate amounts of corona in service, but, as suggested by the authors, some of the new synthetics which have other attractive properties are more susceptible to damage by corona. It is gratifying to note that the authors are working on this problem and will report on it in a subsequent AIEE paper.

E. W. Greenfield (Anaconda Wire and Cable Company, Hastings-on-Hudson, New York): The paper on ionization determination in rubber-insulated cables by Graham, Duffy and Foster is a valuable contribution on the use of this type of equipment. It was not very long ago that the writer, presenting a paper on the use of corona detectors for high-voltage paper cable,[1] was told by a few cable makers that such apparatus was of little value. I am glad to say, however, that the majority of serious investigators in this field have come to look upon corona detection as of paramount importance in the study and production of high-grade insulated cables.

The following discussion will be limited to comments on the paper in the order of the text as presented.

In their first paragraph, the authors inform us that ionization can take place only where a gaseous phase is present. They overlook the fact that ionization may take place in both liquids and solids in the complete absence of voids or gas pockets. The ionization of neutral molecules by such free electrons as are present in a liquid, is the factor which produces the electron avalanches leading to break down. Indeed, the corona detector is a valuable instrument for the study of electron discharge voltages in liquid insulators.

The transient analysis of the Schering bridge, as given by the authors, is interesting. However, it is not quite clear how this applies to the steady-state conditions existing during bridge manipulation. Using the well-known steady-state bridge-circuit theory, the same conclusions, as arrived at by the authors, are readily apparent. Equation 5 in the paper can be satisfied only when the power factor of the standard is the same as that of the sample. Otherwise, an in-phase component of current will appear across the detector terminals. Also in the transient equations the sample is represented as a simple R-C circuit. Actually, the parallel resistance is absorptive in nature, and in general, the simple time constants arising from normal polarization are not obeyed.

With regard to the filter ionization detector, it is not clear how the system rejects the large 60-cycle component, unless this is the function of the wide-band video amplifier tuned to band-pass only frequencies in the megacycle range. If so, why was it found necessary to eliminate the lower frequencies? More information on this point would be appreciated. Also, have the authors made any frequency analysis of the types of discharge present in rubber cables?

With regard to the sensitivity calibration of ionization detectors, I am in agreement with the authors that a uniform calibration scheme would be of value. At our laboratories, we use a 1,000-cycle sine wave, since the filters are adjusted to pass everything from 1,000 cycles up. Also on the matter of calibration, in order to avoid errors due to applied-voltage wave-shape, it has been found desirable to specify peak applied voltage rather than the rms voltage.

Under the discussion of Schering bridge ionization detectors, the authors indicate that such devices could not be adapted to measure earthed cable, and are thereby severely limited in usefulness for testing production and installed cables. I would like to point out that two methods are available for using a Schering bridge corona detector upon earthed cable. The first method involves inverting the bridge and placing

the balancing resistors, condensers, and detector at high potential. The second method and the only one in use in our laboratory is to use the bridge in its conventional form and shield the high-tension leads completely. Such shielding may be connected to the junction of the bridge resistor arms as is also the shield of the high-voltage transformer. All such shielding requires low-voltage insulation from ground.

The use of the probe electrodes, as described by the authors, is very interesting. I am wondering how corona discharge between the probe and the surface of the cable under test can be eliminated, however.

With regard to the coronaless terminal shown by the authors in Figure 12, it is of interest to learn how the exponential flare was arrived at. The purpose of the slope is, of course, to control the stress by maintaining a uniform longitudinal value. We have long utilized a flare which follows a log-log curve, the equation of which is readily derivable. The use of glycerine in the terminal would appear to be a poor selection since this material is completely miscible with water in all proportions and is generally found to give much trouble.

With regards to the statement on ionization volume, I am in accord with the authors in their belief that an integrated pulse count would be highly desirable. We are of the opinion that a vacuum-tube type of wattmeter of sufficiently high sensitivity could be used to integrate the current pulses. This together with a high-impedance voltmeter should provide a measure of both intensity of discharge and the volume of such present.

In the use of the terminals described for production cable testing, are there any checks made to determine the corona point of the high-voltage system and terminals themselves? The bridge method permits ready elimination of system corona and evaluation of terminal discharges. It is essential to design these so that no discharges are present to the top usable limit of the equipment.

Under the section "Ionization Detector versus Power Factor Bridge," it is not clear just how the comparisons were arrived at. It would appear that the authors attempt to check their corona-point determinations by means of power factor versus voltage curves. If so, the two may give decidedly different values, particularly in the presence of moisture.

The data presented on rubber cables during factory tests are very interesting and prove conclusively the value of this tool in searching out weak spots. It is noted that the general level of corona extinction was on the order of 6 to 8 kv for this type of cable. It is of interest to compare these values with corona-extinction points we obtain on similar size cable using our type ABP (butyl-polyethylene) 5-kv insulated cables.[2] Our values range from 9 kv to 16.8 kv for cable with 4/0 American wire gauge copper and 10/64 inch insulation. In many cases for 12/64 inch ABP-insulated cable, we obtain no measurable corona current at voltages up to 20 kv.

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R. C. Graham, E. K. Duffy, and W. P. Foster: Regarding Greenfield's comments the transient analysis is more apropos to corona discharges which are inherently transient. The use of a simple R-C circuit was decided upon in order to show, generally, what happens. In any exact mathematical analysis of response during corona discharge, it will be necessary to consider the transient analysis of the complex R-C circuit which exactly represents the performance of the cable. The filter ionization detector rejects the 60-cycle component by making use of the wide difference in impedance at 60 cycles between the loading capacitor and the radio-frequency coil. Further rejection is obtained by tuning the amplifier.

The authors stated that the Schering bridge ionization detector has the disadvantage of not being adaptable to the measurement of earthed cables, such as would be encountered for production tests in grounded water tanks. It is recognized that methods are available for overcoming this disadvantage in laboratory equipment. However, where laboratory tests are concerned, it is a simple matter to insulate the cable under test from ground.

The primary purpose of the probe electrode is to detect the exact localized area of low ionization within unshielded cables, or to determine the relative values among several similar cables by setting the applied voltage at a predetermined value which is considerably below that which would represent the ionization voltage between the electrode and cable; full reel lengths of cable can be checked by this nondestructive test. No attempt is made to determine the true ionization level of the cable as this can be readily accomplished on the entire length as described in the text.

As Greenfield points out, the exponential flare incorporated in the ionization-free terminal has been in use for a long time and is readily derived. However, the unique method of incorporating an elastic semiconducting material which adapts itself to a wide range of a different size cable is described for the first time in this paper, since there has not been any practical method of terminating cable lengths immersed in grounded water tanks for testing, on a production basis. The use of glycerine to absorb any formation of corona energy was resorted to mainly because of its high dielectric constant and the fact that it is miscible with water which permits the cable ends to be readily cleaned.

The general level of corona extinction was in the order of 6 to 8 kv, based upon factory production tests of insulated rubber cable. On laboratory measurements involving short lengths of similar type oil-base insulated cables, values of the order referred to for type ABP insulation have been obtained.

Results of Tests Carried Out at the 500-Kv Experimental Station of Chevilly (France), Especially on Corona Behavior of Bundle Conductors

FRANCOIS CAHEN
MEMBER AIEE

THE 500-kv experimental station of Chevilly, near Paris, France, has been built with a view to making a close study of the behavior of the lines to be used when building up the French 400-kv system. Present economic conditions in France do not favor the use of copper conductors. In consequence, two solutions have been considered for the equipment of these lines.

1. Utilization of large diameter (1.58 to 1.97 inches) expanded aluminum-cable-steel reinforced (ACSR) conductors.

2. The use of a bundle of two conventional ACSR (1.04-inch-diameter) conductors, the total cross section of which is equal to the economic section.

To facilitate at the opportune moment the rapid layout of a 400-kv system, while meeting at the same time the needs for extension of the existing 230-kv system, the new high-capacity transmission lines of the latter are being designed in the form of double-circuit 230-kv 6-conductor lines laid on a horizontal plane and convertible into single circuit 400-kv lines. The Le Breuil-Chevilly line was designed along these lines. This line is 252 miles long and is equipped with six standard 0.638-square inch ACSR conductors. This line was put in operation in December 1946 (Figure 1).

If single large diameter conductors are utilized, when this line is converted into a single 400-kv line, the six conductors now in use will have to be taken down. If, however, the bundle technique is adopted, the conversion of the line will be obtained through a mere regrouping of the six conductors now in operation into three bundles of two conductors each. This second method is not only quicker than the first but offers the advantage of utilizing only standard cables.

The station of Chevilly was built mainly for carrying out experiments and comparisons on the two methods as regards corona losses and radio influence under all kinds of atmospheric conditions; for selecting the optimum operating voltage; for determining, when utilizing bundle conductors, the most favorable spacing between conductors as conditioned by the corona effect, the sticking of conductors under the effect of the wind, electrodynamic stresses due to short circuits, their disposition either on a vertical and horizontal plane, and finally, for studying insulators and line hardware.

Description of Experimental Station

The station has been erected close to the 230-kv Chevilly substation located in the suburbs of Paris, 300 feet above sea level.

It consists of a single-line span the length of which, 1,640 feet, is equal to the mean span of the convertible Le Breuil-Chevilly line. The end towers of the span are conventional suspension towers (Figures 2, 3, 4). The phase spacing is 47.5 feet. The line is anchored to the ground at both ends by means of guys linked to the line conductors by double insulator strings. Each suspension string carries 24 insulators (diameter 11.2 inches, spacing 6 inches). Suspension and dead-end strings are provided with grading rings. Two ground wires, located 46 feet above the conductors, may be, either insulated or grounded. Wooden trestles have been erected below the three phases in order to keep the conductors off the ground when being laid down and thus avoid their getting soiled.

A bundle is composed of two standard 0.638 square inch ACSR conductors (aluminum cross section 0.505 square inch, outer diameter 1.04 inch). They are supported by clamps fixed on an adjustable link attached to the lower insulator fitting. Conductors may be laid either side-by-side at the same level (horizontal layout) or one above the other (vertical layout). "Bakelised" wooden spacers may be placed between the conductors at one or several points of the span.

Figure 1. Standard double circuit 230-kv suspension tower

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FRANCOIS CAHEN is manager, systems studies and research division, Electricite de France, Paris, France.

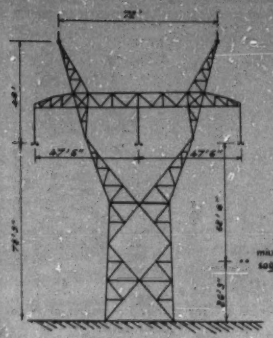

Figure 2. Standard single circuit 400—500 kv suspension tower

Three kinds of expanded ACSR conductors will be tested with diameters of 1.57, 1.77, and 1.97 inches.

The supply station (Figures 5 and 6) includes three 106-kva, single-phase transformers; the 11-kv delta-connected low-voltage windings are provided with no-load tap changers which make it possible to obtain a no-load high-voltage range of from 178 to 289 kv (phase-to-neutral voltage) or from 308 to 500 kv (phase-to-phase voltage) in 64 steps.

The high-voltage windings are star connected, the neutral being solidly grounded.

It was deemed advisable to give the line the length of a single span so as to facilitate the systematical study of meteorological conditions and make the replacement of conductors easier. The output of the transformers has been limited to that which was strictly necessary to keep the line energized. This allows the reduction of transformer losses and also the checking of some high-voltage measurements by the help of low-voltage measurements. In this way the station was set up with a maximum of economy not inconsistent with the securing of accurate results.

Besides the main experimental line, another single-phase, insulated line, 36 feet long, has been pulled along the axis of a grounded cylindrical lattice 79 inches in diameter. This feature gives the possibility of rapid tests on short lengths of conductors and of a close examination of corona effect phenomena.

The voltage supply of this line is such that the conductors are subjected to the same surface gradient as those of the main lines; atmospheric precipitations get through the lattice, which in addition can be sprayed with artificial rain.

Finally, in order to study the influence of air density on the corona effect, two more experimental stations have been built one at Chevilly, close to the 500-kv line, and the other in the Alps at Alpe d'Huez, 6,000 feet above sea level.

These two stations, identical in design (Figures 7 and 8), include a 330-foot span of 230-kv, single-phase line fed by a potential transformer at a 133-kv phase-to-ground voltage. To obtain, under single-phase operation, a surface gradient equal to that of a normal 3-phase line, conductors having a very small diameter (ASCR 0.23 square inch, outer diameter 0.62 inch) have been utilized. In addition to this, another grounded line identical to the first has been strung parallel to the first one. This second line makes possible the comparison of the weathering of live conductors with that of dead conductors.

Program of Tests

The tests which were initiated in December 1946 and which have been going on since without interruption, have been carried out according to the following plan:

Corona Losses

Bundle Conductors:

Effect of voltage on corona losses; corona curves.

Influence of weathering of conductors on losses.

Influence of atmospheric and weather conditions on losses and uninterrupted recording of losses under constant voltage.

Influence of spacing of bundle conductors on losses.

Comparison between vertical and horizontal position of conductors in bundle.

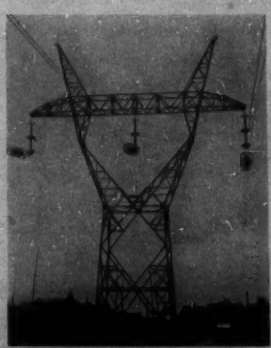

Figure 3. Standard single circuit 400—500 kv suspension tower

Figure 4. 500-kv experimental line with a span length of 1,640 feet

Influence of ground wires.

Comparison of losses on each of the three phases among themselves; comparison of losses in single, 2 or 3-phase supply.

Measurement of insulator string losses.

Expanded Conductors:

Same tests as indicated before except with regard to the influence of spacing and position of bundle conductors.

Influence of Air Density (230-Kv Experimental Stations):

Effect of voltage on corona losses; corona curves.

Weathering of conductors.

Influence of atmospheric and weather conditions on losses and uninterrupted recording under constant voltage.

Study of Sticking of Bundle Conductors Under the Effect of the Wind and Short Circuit Currents

Horizontal and vertical lay-out.

Radio Disturbances

Variation of disturbing field with: voltage; atmospheric conditions; type of conductors; and distance from the line.

The measurements are made in the vicinity of the 500-kv, experimental line, the 230-kv single-phase, experimental line and several 230-kv lines of the French system now in operation.

Results of Corona Losses Measurements

The processes utilized for the measurement of corona losses, as well as a part of the results of tests carried out in 1947, were described in three previous reports.[1,2,3]

The sum of the results secured up to May 1, 1948 will be recapitulated and commented hereafter.

Figure 5. The 500-kv test station of Chevilly

Figure 8. The 230-kv test station of Alpe d'Huez, 5,900 feet above sea-level

General Scheme For the Carrying Out of Tests

Two processes were applied:

—Plotting, by points, the curves of losses versus voltage. This process is practicable only in dry weather. In bad weather, especially in the case of rain or snow, losses are generally too unstable to make for an accurate plotting of curves. Even in dry weather, it is necessary to act very quickly as losses vary sometimes within an hour's time. This is due to the influence of temperature changes on air density and height of conductors above the ground and the effect of dust deposit on conductors in relation with the direction and speed of wind.

Curves of losses versus voltage are used mainly to study the weathering of cables, the influence of air density, the comparisons between various spacings (bundle), different diameters of conductors (expanded cables) and studies on the electric field.

Uninterrupted recording of losses under constant voltage. These recordings are utilized mainly to study the influence of weather conditions (rain, snow, fog, sleet) upon losses and to make comparisons between conductors of different spacings and diameters when subjected to these conditions.

These tests are supplemented by illustrations of brushes, which were photographed at night (Figures 9 and 10).

It would be useful to carry out all these measurements under a 3-phase supply, the three phases being provided with the same conductors, so as to work under the same conditions as for industrial operation.

But this process entails long delaying periods, owing to the length of time necessary for the weathering of conductors (from three to six months) and the large number of dispositions and types of conductors that would have to be tested. Moreover when two different layouts have been successively tested, it is impossible to make sure that the weather conditions they were subjected to were the same in both cases.

Figure 6. The 500-kv test station of Chevilly showing single-phase transformer and capacitive divider for voltage measurements

Figure 7. The 230-kv test station of Chevilly, 300 feet above sea-level

It has therefore been found preferable for comparisons between both types and dispositions of conductors to proceed as follows:

The outer phase, 3, equipped with two conductors in horizontal layout with a spacing of 16 inches, is selected as a basis and suffers no alteration all through the test period. Changes in the disposition (bundle) or changes of conductors (expanded) are carried out on the other two phases, 1 and 2.

In order to compare the different dispositions of bundles, the changes are carried out in succession on the other outer phase 1. The outer phases 3 and 1 alone are alive, the median phase 2 being grounded.

For the comparison between different types of expanded cables, these cables are laid both on median phase 2 and on the extreme phase 1, the reference phase 3 continuing with a bundle equipment. The three phases are energized.

But this implies that for tests 1 it may be possible to obtain results under a 2-phase supply which would be equally valid under 3-phase supply. This also supposes that for tests 2 it may be possible to separate the losses due to each one of the phases and deduct therefrom the value of 3-phase losses that would have occurred should the equipment have been homogeneous.

Interpretation of Tests

Transposition of Loss Measurements From the Single and Double Phase to the 3-phase Supply:

This transposition is made easy by assuming that for a given conductor (or system of conductors), under given meteorological conditions, the value of losses is related to the value of the theo-

Figure 9 (left). Night photograph of the line showing some brushes on the bundle conductors

Phase - to - ground voltage 305 kv under two-phase supply

Figure 10. Night photograph of one expanded-type 1.77 inch conductor

Phase - to - ground voltage 285 kv under single-phase supply

retical surface gradient. In the case of a single conductor, the calculation of the gradient is easily made by applying electrostatic laws; it proceeds directly from the value of capacitance factors which can be both calculated and measured. In the case of a bundle of conductors, it is necessary, as will be shown later on, to take into account an equivalent surface gradient, which can also be calculated from the capacitance factors.

Should this assumption prove correct, the plotting of the curve of losses versus voltage, carried out, for instance, under a single-phase supply on one of the phases of the line, makes possible the drawing of the corresponding curve under 3 phase supply. In fact, by dividing the abscissae of the single-phase curve by the ratio of theoretical gradients under 3- and single-phase operation, the ratio being calculated to the value of capacitance factors, the new curve of losses is obtained for 3-phase operation. The loss curves for the other two phases are determined in a similar way if allowance is made for the fact that the two outer phases are theoretically submitted to the same gradient while the central one is submitted to a higher gradient.

Table II gives as an illustration of the foregoing, the coefficients to be applied at Chevilly, in the case of a bundle of two conductors, with a spacing of 16 inches at a supposed temperature of 20 degrees centigrade.

Experimentation has particularly consisted in checking, in the shortest time possible, the curves of losses of the line under single- and 3-phase supply and in comparing, for each phase, the losses measured under each different supply. This implies that the problem of the separation of losses under 3-phase operation studied further has been solved.

It has been possible to observe that the coincidence of the plotted curves of loss versus surface gradient was satisfactory without being perfect. That depends, first of all, on the difficulty of experimentation arising from the instability of loss curves even in dry weather. Moreover, below the critical voltage, curves are often ill defined while above this voltage the capacitance factors (consequently the theoretical gradients) are modified by space charges as shown by the curves on Figure 11. These were checked during fair weather and rain on one of the 230-kv experimental lines.

A much better coincidence of curves is obtained by utilizing the measured capacitance currents as abscissae instead of the gradients, as shown on Figure 12. The near coincidence of single and 3 phase curves will be noticed for each one of the phases 2 and 3, both equipped with bundle conductors. The difference between phase 2 and phase 3 curves is caused by the fact that the second one is more weathered than the first.

From the sum of researches carried out, the conclusion may be drawn that the error on the gradients resulting from the use of theoretical switchover factors does not exceed 4 per cent.

Division of Losses:

The reading of the individual wattmeters of the three phases does not give an accurate indication of losses pertaining to each particular phase, even after making the corrections rendered necessary by the reactive power flow between the phases. In reality, the watt component of a phase current is the amount of watt current which flows into the ground plus the watt current exchanged between the phases, which depends both on the value of the losses of the phase under survey and the losses of the other phases. The division of losses may be effected by assuming that the loss angle has the same value in all directions which is the same as assuming the proportionality of loss currents and capacitive currents. Experimentation has shown this assumption to be but roughly approximate. However,

Table I

	Sticking Current, Amperes				Unsticking Current, Amperes			
	Computed		Measured		Computed		Measured	
	First Method	Second Method	Minimum	Maximum	First Method	Second Method	Minimum	Maximum
Horizontal layout								
1,640 feet span								
Distance, inches 16	746	812	920	970	540	644	620	640
Distance, inches 24	1,118	1,216	1,140	1,280	736	882	730	770
820 feet span								
Distance, inches 16	1,492	1,624	1,420	1,630	1,080	1,288	1,140	1,260
Distance, inches 24	2,236	2,432	1,900		1,476	1,764	1,300	
Vertical layout	Minimum	Maximum	Minimum	Maximum	Minimum	Maximum	Minimum	Maximum
1,640 feet span								
Distance, inches 16	1,520	1,820	1,650	1,730	1,100	1,320	1,180	1,250
Distance, inches 20	1,900	2,270	2,150	2,240	1,305	1,560	1,420	

as the error likely to be made concerns only a corrective term, this method of dividing the losses has been found sufficiently accurate.

Case of Bundle Conductors: Influence of Their Spacing and Layout

Theoretical Study:

The theoretical study of the surface field of a bundle of two smooth conductors has been made in a previous report.[1]

Supposing two insulated conductors brought to the same potential V_0 in relation with the ground (Figure 13).

Let:

r = conductor radius
D = distance between conductors' axes
h = average height of conductors above the ground.

The capacity per unit length of the line in relation with the ground is:

$$c = \frac{1}{2\ln\frac{2h}{\sqrt{rD}}}$$

The mean surface gradient is:

$$E_m = \frac{V_0 c}{r}$$

The potential gradient at a point M of one conductor's surface is:

$$E = E_m\left(1 + \frac{2r}{D}\cos\alpha\right) \qquad (1)$$

This gradient is minimum in B:

$$E_B = E_m\left(1 - \frac{2r}{D}\right)$$

It is maximum in A:

$$E_A = E_m\left(1 + \frac{2r}{D}\right)$$

For $r = 0.52$ inch; $D = 16$ inches, the extreme relative variation is $\doteq 6.6$ per 100.

On the other hand, the calculation shows that the presence of two adjacent phases, under 3-phase operation, does not modify the law of variation of the surface gradient, owing to the great distance between phases. Its only effect is to increase the load on each one of the conductors, and consequently the capacity per unit length c and the mean gradient E_m.

If the spacing of the conductors increases, the minimum and mean gradients are constantly on the increase and the maximum gradient goes through a minimum which in the case of the Chevilly line occurs at a spacing of 11.4 inches (Figure 14).

Corona losses are closely related to the value of the theoretical surface gradient (at least, in so far as the latter does not exceed by too great a margin, the critical voltage above which space charges modify the outline of the field). In the case of bundle conductors, however, the surface gradient not being constant at the periphery of the conductors as shown in equation (1), the law of loss variations versus conductor spacing is not that which determines the variation of the maximum gradient. This was rendered manifest by the tests.

The theoretical study also shows that surface gradients are the same whether conductors are laid horizontally or vertically.

Practical Results:

The experimental survey was carried out in the manner described before. It included the checkings by points of loss curves versus voltage and recordings under constant voltage in the rain.

Losses vary but very little when the spacing of conductors lies between 8 and 16 inches; the minimum of losses in good and bad weather corresponds to a spacing of approximately 8 inches. The law of loss variations has been observed in this case to be the same, as if on the surface of each conductor a constant gradient prevailed equal to the mean of the maximum and mean gradients represented by the equation:

$$E_e = E_m\left(1 + \frac{r}{D}\right)$$

The variation of this equivalent gradient versus spacing is figured by the dotted curve on Figure 14. It is minimum for a spacing of 7.5 inches which tallies with experimental results.

Beyond 16 inches, losses keep on increasing more and more rapidly. At 32 inches they are at least four times higher than at 16 inches.

Losses are the same regardless of the weather, whether conductors are laid horizontally or vertically.

Weathering of Conductors—Losses by Weather

The conductors are pulled without undergoing any preliminary cleaning process, as would be done when building a line, but special precautions are taken while they are being pulled. The well-known phenomenon of weathering is then observed, and manifests itself in a progressive reduction of losses. This

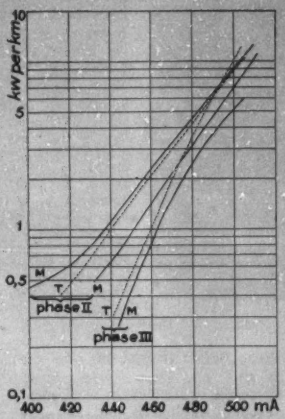

Figure 12. Transposition from single-phase to 3-phase supply on phases II and III equipped with bundle conductors. Corona losses per phase and per kilometer versus capacitive current

M = single-phase supply
T = 3-phase supply

Table II

	Switch-over From Single-to-3-Phase	Switch-over From 2-to-3-Phase
Phases 1, 3	1.07	1.05
Phase 2	1.11	1.09
Switch-over from phases 1 and 3 to phase 2		
Single-phase operation		1.01
Three-phase operation		1.05

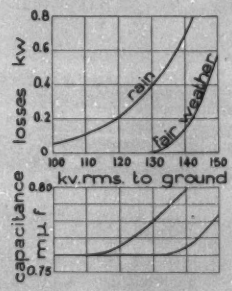

Figure 11. 230-kv test station

Effect of voltage variation on losses and ground capacity

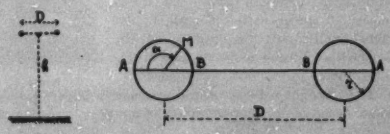

Figure 13. A single phase line with a bundle of two conductors

reduction is important only if the conductor is kept alive.

Weathering has been observed (Figure 15) on a bundle of two conductors as well as on a line with single 1.77-inch conductor and on the two 230-kv installations of Chevilly and Alpe d'Huez.

The value of 3-phase losses on weathered conductors during dry weather and no fog, for a bundle of two conductors with a spacing of 16 inches is as follows:

400 kv—less than	0.8 kilowatt per mile
420 kv—	1.1 kilowatt per mile
440 kv—	1.6 kilowatt per mile
460 kv—	2.7 kilowatt per mile

For the first 1.77-inch expanded cables, which are being experimented upon, losses, after three months' weathering, are about ten times as high as those of the bundle. According to theoretical calculations however they should be about the same.

Influence of Ground Wires

The presence of ground wires increases theoretical surface gradients by one to two per cent, which is confirmed by loss measurements.

Losses Due to Insulator Strings

Measurements of insulator string losses carried out during all kinds of weather have shown that these losses always remain very small in comparison with the corona losses of conductors. Total losses due to the insulator strings of a line operated at 400 kv (1,640 feet span, two strings per phase) calculated from experimental results should be about 10 per cent of conductor corona losses.

Corona Losses by Bad Weather

Numerous recordings of constant voltage losses have been carried out on the experimental line under the most varied atmospherical condition (rain, snow, fog, sleet) and have emphasized a well-known phenomenon, the increase in corona losses which at times becomes considerable.

The plotting of loss versus voltage curves, although very difficult to carry out accurately during bad weather, shows the relative increase in losses to be more important below than above the critical voltage. For very high gradients, losses in dry and bad weather have a tendency to become somewhat similar.

In the course of recordings carried out in all weather from January 1, 1947 to April 30, 1948, the highest peak losses were recorded in the spring of 1947 at the beginning of heavy rain storms. In the case of rain storms a very swift rise in losses is observed at the beginning followed by a peak lasting only a few minutes. Afterwards losses decrease and keep on being pretty irregular. When the shower is over, losses decrease during the drying process according to a law of exponential shape. Figure 16 illustrates some typical recordings obtained on bundle conductors (with a spacing of 16 inches) and on an expanded cable 1.77 inches in diameter. The peaks of losses recorded at the time of snowfalls are not so high as in the case of heavy rainfalls. This has been observed both at Chevilly and at the high altitude station. The same thing occurs during foggy weather.

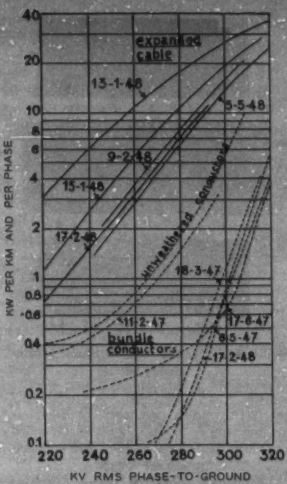

Figure 15. Corona losses versus phase-to-ground voltage during good weather under single-phase supply showing influence of weathering and difference between bundle conductors (16 inches spacing, lower curves) and expanded conductor (1.77 inch diameter, upper curves)

The highest peak losses recorded in the above mentioned period, corresponding to 16 months' experimentation on bundle conductors (16 inches spacing), brought back to 3-phase supply were as follows:

397 kv—105 kilowatts per mile
413 kv—130 kilowatts per mile

With an expanded cable 1.77 inch in diameter (three months' experimentation), losses in bad weather are about double those of bundle conductors (16 inches spacing).

A test has been made to correlate the energy wasted in losses under a shower and the height of rainwater recorded by the pluviometer. This correlation is not bad as can be seen on Figure 17. It shows an average loss of 14.5 kilowatt-hours per mile and per millimeter of rainwater for the center phase equipped with two bundle conductors with a spacing of 16 inches, under a voltage corresponding to 3-phase 400-kv. The corresponding 3-phase loss would be 40 kilowatt-hours

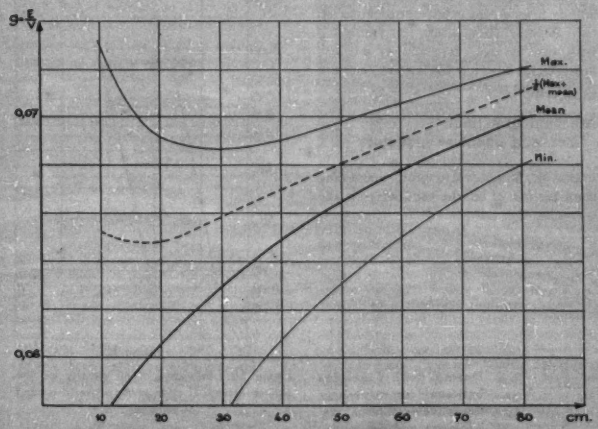

Figure 14. Bundle conductor. Effect of conductors spacing variation on maximum, equivalent, mean and minimum surface gradient, under single-phase supply

$$g = \frac{\text{surface gradient kv per centimeter}}{\text{phase-to-neutral voltage, kv}}$$

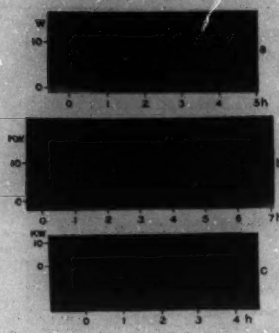

Figure 16. Typical corona losses records during rain

(Losses in kilowatts for a 1,640 foot span)
(a) and (b) records were taken simultaneously
a. February 7, 1948, 1.77 inches expanded conductor, single-phase supply, 250-kv phase-to-ground voltage
b. February 8, 1948, 1.77 inches expanded conductor, 3-phase supply, 400-kv phase-to-phase voltage
c. February 8, 1948, two 1.04 inches standard conductors, spacing 16 inches, 3-phase supply, 400-kv phase-to-phase voltage

per mile and per millimeter of rainwater at 400 kv.

Influence of Air Density

Study of air density δ influence on corona losses has been systematically carried out at the 500-kv Chevilly station. This study has shown as a first approximation[1] that the voltage corresponding to a given value of losses varies in direct proportion to δ. But this verification has not seemed sufficient on account of the slight variation of the density in relation to meteorological conditions (temperature, pressure under constant altitude). That is why, with the object of building high altitude lines, the two single-phase 230-kv stations described before have been erected.

The tests, carried out in these stations, included both the plotting of loss versus voltage curves and uninterrupted recordings made under constant voltage, by means of automatic recorders. Figure 18 shows, on the one hand, loss curves in dry weather as checked in both stations without any air density correction and, on the other hand, the same curves corrected by the value of air density by applying the law of Peterson.[4] This law states, that if the critical voltage is not deviated from by too great a margin, the voltage corresponding to a given value of losses varies in direct proportion to $\delta^{2/3}$. Under these conditions the curve concordance is excellent. The confirmation of Peterson's formula by the tests is all the more interesting as the difference between air densities is greater:

$$\frac{\delta \text{ Alpe d'Huez}}{\delta \text{ Chevilly}} = 0.8$$

Recordings during rain have shown, that under an equal voltage, the increase in losses due to high altitude is far less important than in dry weather.

If these results are applied to the 400-kv line, the values of losses versus altitude (bundle conductors 16 inches spacing) as shown in Table III are obtained.

The study of radio disturbances shows that disturbances caused by the 400-kv line are, at a voltage of 400 kv, of about the same importance as disturbances observed in the vicinity of the 230-kv lines recently put in operation on the system. They keep on increasing with the voltage, but not so quickly, however, as losses. At 440 kv they are higher by 80 to 90 per 100 than at 400 kv.

Mechanical Actions Between Bundle Conductors

One of the important problems arising from the use of bundle conductors is the closing up of conductors under the effect of high winds and short circuits. An accidental contact between conductors is not dangerous but it is necessary to avoid their sticking together, that is, their remaining in contact along an important length owing to an aerodynamic effect in case of high wind, or to electrodynamic attraction stresses likely to persist after elimination of a short circuit on the system, not directly affecting the involved line (load transfer after elimination of a parallel line affected by a fault). A protracted sticking would in fact bring about an important increase in corona losses and radio disturbances and risk damaging the conductors.

The detailed, theoretical study of closing up phenomena following a short circuit, that is, sticking and parting in the case of two bundle conductors laid horizontally has been together with the re-

Table III

Altitude, Feet	Three Phase Losses at 400 Kv Kilowatt per Mile: Dry Weather	Average Rain Shower
300	0.8	20
3,300	2.1	28
6,000	7.2	40

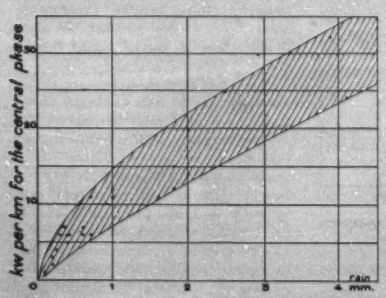

Figure 17 (left). Correlation between corona energy losses (kilowatt-hours per kilometer and per phase) and height of rain water (millimeters) during a shower

Phase-to-phase voltage 400 kv under 3-phase supply

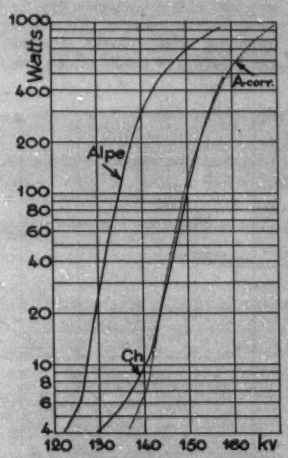

Figure 18. Influence of air density on the corona losses. (Losses versus phase-to-ground voltage)

Ch—loss-curve at Chevilly, altitude 300 feet
Alpe—uncorrected loss-curve at Alpe d'Huez, altitude 5,900 feet
A-Corr.—same curve after having been corrected according to Peterson's formula

Table IV. Minimum Number of Spacers Required to Avoid Sticking

Spacing of Conductors, Inches	Lay-out of Conductors	
	Horizontal	Vertical
12	2	1
16	2	0
20	1	0
24	1	0

sults made of tests carried out for the verification of its validity. The theoretical study with regard to bundle conductors laid vertically has also been made and its correction confirmed by a series of quite recent tests.

For the computation of sticking and parting currents, it is necessary to take into account electrostatic repulsion stresses and electrodynamic attraction stresses. When the line is on no-load, only the former occur and cause the spacing between conductors to increase. When the line is loaded, the spacing decreases and the balance of the stresses, expressed by the parallelism of the conductors, is reached when the line is operated at its surge impedance load. With a short circuit, the attraction stresses alone occur or, at least, are largely in excess.

Table I gives some of the results obtained when supplying conductors with direct current of variable intensity and shows, opposite, the results of the theoretical study.

Fair agreement between experimental and calculated results points out the possibility of taking the latter as a basis for finding out the minimum number of spacers required to avoid a continuous sticking of conductors after elimination of the short circuit. This has been obtained by fixing at 1,300 amperes (900 megawatts at 400 kv) the intensity likely to be carried by the line after elimination of a short circuit on a parallel line. The minimum number of spacers required per 1,640 feet span are shown in Table IV.

Recordings of closing-up of conductors by high wind have shown that with horizontal layout, two spacers at least for a spacing of 16 inches and one spacer for 24 inches were required to avoid sticking. Vertically, closeups are practically nil; no spacer is required for spacings of 16 and 20 inches.

General Conclusions

A general survey of the theoretical studies and tests, which have been carried out for the last 18 months, leads to the positive conclusion that it is possible to operate a line of the same type as the experimental line of Chevilly under a phase-to-phase voltage, equal at least to 400 kv, 50 cycles.

Should the technique of two bundle conductors be adopted, the right spacing will be 16 to 20 inches, with which corona losses are but little over the minimum corresponding to the conductors utilized (1.04 inch diameter) and for which the minimum number of spacers per 1,640-foot span does not exceed two, for conductors bundled in a horizontal layout. When the bundle is laid vertically no spacer is required which makes this layout more advantageous. It means, however, slightly higher towers and consequently an increase in the cost of erection of the line.

With an operating voltage of 400 kv, losses during good weather are lower than 0.8 kw per mile, and are comparable to those on existing 230-kv lines. They are therefore much lower in comparison with the transmitted power. Losses during bad weather are much higher than in fair weather. If allowance is made for the alternate spells of dry and wet weather, the total amount of power lost through corona losses is perfectly admissible.

Based on observations made at Chevilly on a survey of the route covered by the le Breuil-Chevilly line (250 miles long) and on observations of the rainfall and the differences in altitude along this same route, it is estimated that the average corona losses per annum would be 4 kw per mile, if this line were operated under a mean voltage of 400 kv. Under 440 kv losses would be two to three times higher which still would be admissible.

In reality the limitation of the operating voltage will depend on radio disturbances. It is not at all certain that it may be possible in this respect to go much over 400 kv, at least not without increasing the diameter of conductors at certain points.

With regard to expanded conductors, experimentation has not been going on long enough to be able to draw definite conclusions on the subject. All types of cables have not yet been tested. For the time being it appears that expanded conductors seem to give higher losses than the bundle conductors, especially during dry weather.

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4. Discussion by W. S. Peterson of Design of Transmission Lines, J. S. Carroll, B. Cozzens. AIEE Transactions, volume 52, 1933, pages 55–63.

Discussion

M. Temoshok (General Electric Company, Schenectady, N.Y.): An important contribution of this paper is the correlation between the variable surface voltage gradient of the bundle conductors and the corona loss; that is, the use of the equivalent constant gradient as being equal to the average of the mean and maximum gradients. In establishing such a criterion, it should be noted that when the ratio of spacing between bundle conductors to conductor diameters becomes less than five, the approximate equations used in the paper for the solving of the surface voltage gradients become an error. This error arises because the field produced by one conductor in the vicinity of the other is no longer parallel, an assumption of the approximate equation.

Mention is made of experimental losses in good weather being ten times greater for the single-conductor expanded-cable of 1.77 inches in diameter than for the bundle conductors of 1.04 inches diameter. This in itself is an astonishing result, as the surface voltage gradient of the single conductor is appreciably less than that of each conductor of the bundle conductors. Since the surface voltage gradients of the bundle conductors were illustrated, it would have been of interest to have indicated the surface voltage gradient of the single conductor used in the comparisons.

All results dealing with surface voltage gradients have been checked closely by results of similar calculations made by the writer, with one exception. The values of Figure 14 of *g*, the surface voltage gradient kv per centimeter per phase kv, seem to be lower than expected for a 3-phase calculation. As the "Theoretical Study" discussion presents only the equations for surface voltage gradients of two conductors at the same potential above ground, it would appear that Figure 14 would be the result of calculations using these equations, and does not represent an actual 3-phase case.

P. L. Bellaschi (Consulting Engineer, Sharon, Pa.): In recent years, considerable experimental work has been conducted in Europe, directed to the development of a-c power transmission at extra-high voltages. The corona studies at Chevilly, France, in particular have been outstanding in this connection, and we are

fortunate in having the paper by François Cahen. Likewise, there have been noteworthy contributions in this field in Sweden and in Switzerland. In fact, the entire subject of power transmission at extra-high voltages was a topic of extensive and constructive discussion at the recent International Conference on Large High-Voltage Electric Systems held in Paris, June 24 to July 3, 1948.

In Sweden, a 380-kv line has been projected from the Harspranget Waterfalls to the south, the center of consumption, some 960 kilometers in length. Here, too, bundle conductors have received preference over the expanded or hollow conductors. To minimize corona loss and radio noise, steel-reinforced aluminum cable conductors, 1.26 inches in diameter, spaced 18 inches apart in a 2-bundle arrangement and disposed horizontally have been selected. The larger conductors have been justified in addition for economic reasons, that is, capitalization of copper losses and so forth. Apparently, operating conditions prevailing in that country, such as sleet and ice formation, have been among the factors influencing the selection of the horizontal arrangement.

In view of the preference apparently being given to the vertical arrangement, as indicated in the paper, it may be in order to pose the question whether sleet, ice formation, and similar difficulties are likely to be a matter of practical concern with this arrangement for the conditions likely to be encountered on the proposed 400-kv lines in France.

Another point which presents itself is the matter of relative advantage of bundle and expanded or hollow conductors from the corona standpoint. According to Figure 15 of the paper, bundle conductors are at an advantage over the expanded or hollow conductors. However, this comparison does not appear to be a final one, as it would seem some of the expanded and hollow conductors tested at Chevilly could well stand further improvements from a manufacturing standpoint, thus minimizing some of the irregularities present in the original conductors tested. Additional tests on improved conductors should be helpful in clarifying this point. This apparently is the intent of the program at Chevilly, as intimated in the paper.

One can state quite objectively that the contributions by Cahen and his associates mark an important and outstanding milestone in the field of high-voltage corona studies. In the light of the findings at Chevilly, France, and elsewhere, one can feel with definite assurance that the major questions and technical difficulties which presented themselves to the minds of transmission engineers several years ago in regard to the corona problem at extra-high voltages are being rapidly overcome in a masterly and practical manner. Additional experimental data are, of course, desirable in order to clarify certain remaining questions. A final decision on some of these points probably cannot be reached other than by actual experience which, in view of the developments in course in Sweden and France, should not be too long forthcoming.

A. D. Pettee (General Cable Corporation, New York, N.Y.): The author, in Figure 15, indicates that the expanded 1.77-inch conductor has much higher loss than the bundle conductors. The expanded conductors which he tested, according to my source of information, were not conductors with as smooth an external surface as can be secured with the hollow-core (HH) construction as produced in this country.

Table I

Diameter of a Single Conductor, Inches	g
1.57	0.0705
1.77	0.0638
1.97	0.0585

Computation by the Carroll-Rockwell method[1] with an air density of 1.0, which, presumably, was substantially the condition in Cahen's tests, indicates that the 1.77-inch type-HH conductor would show losses of the same general order of magnitude as the bundle conductors, rather than the high losses for expanded conductors which Cahen has chosen to provide his comparison. It is important to know definitely whether Cahen's expanded conductors had a smooth, completely interlocked, external conductor layer or had an external layer of flat strips not thoroughly interlocked. If the latter is true, and there was an associated lack of smoothness, the high losses shown for the expanded conductor are not representative of the low losses which would be found with a smooth conductor such as the HH type.

I wish also to call attention to the drooping characteristics of the expanded cable loss curves in Figure 15 which contrast with the rising curves which are shown for the bundle conductors, and are also shown for the conductors studied in the Carroll-Rockwell paper. If the external surface of Cahen's expanded cable was not definitely smooth, it may explain the high losses in the lower part of his curves for expanded cable.

It is apparent that Cahen is correct in his statement that experimentation has not been going on long enough to be able to draw definite conclusions on the comparison between expanded and bundle conductors. In this connection, it is expected that the Tidd Station corona-loss tests now under way at Brilliant, Ohio, will furnish further important data on the relative losses of expanded and bundle conductors, and will supplement such data as Cahen has submitted for our review at this meeting.

REFERENCE

1. EMPIRICAL METHOD OF CALCULATING CORONA LOSS FROM HIGH-VOLTAGE TRANSMISSION LINES, J. S. Carroll, M. M. Rockwell. ELECTRICAL ENGINEERING (AIEE TRANSACTIONS), volume 56, 1937, pages 558–65.

François Cahen: In answer to the discussion by Temoshok, I agree with him that mean maximum, minimum, and equivalent gradient formulas are no longer valid when the spacing between bundle conductors becomes insufficient when compared to their diameters. As a result of tests carried out on short lengths in a test cage, the approximation still holds good for a spacing between conductor axes equal to three times their diameter; on the other hand, in actual practice, spacings to be taken into consideration are much greater. For instance, minimum losses were obtained with a spacing of about 8 inches, that is, nearly 8 times the diameter of the conductors, and for reasons of a mechanical character, a spacing of at least 16 inches should be adopted on industrial lines.

With regard to the bearing of single conductors, the calculation of the theoretical surface-gradient is easy to make. For the experimental line at Chevilly, the ratio

$$g = \frac{\textit{surface-gradient (kv per centimeter)}}{\textit{phase-to-neutral voltage (kv)}}$$

would represent, under conditions identical with those for which curves on Figure 14 were computed, the values in Table I.

Therefore a cable of 1.77 inches gives a theoretical gradient slightly lower than the minimum equivalent gradient of the bundle.

The fact that this cable has given, when under test, much higher losses than those of the bundle is assuredly due to the bad surface condition of the conductor, attributable to the disjunction of strands in the outer layer, but due also to the fact that the air disruptive gradient on the conductor surface decreases, as Peek has already shown, when the conductor radius increases. This results in a decrease of the critical voltage and an increase of losses independent of any disjunction.

In answer to the discussion by Pettee, I shall state that the bad surface condition of 1.77-inch diameter conductor is certainly responsible in a large proportion, for the very high losses shown on Figure 15. Since the report was written, many other tests have been carried out, both on the line and in a test cage, on cables 1.77, 1.89, and 1.97 inches in diameter. The tests on short lengths carried out in a test cage eliminated completely disjunction of outer strands and permitted a careful cleaning of the surface of the cables. These tests have shown that under the best surface conditions, losses of single conductors in dry weather were still, after undergoing a thorough weathering process from five to ten times higher than those of the bundle, and that during rain the losses of single conductors were about double those of the bundle.

It seems, therefore, almost certain that the high value of losses of expanded cables arises independently of the disjunction of outer strands from a systematic cause; namely, the fact previously pointed out, that the air disruptive gradient decreases when the conductor radius increases. Moreover, the decrease of the field, when getting away from the conductor, becomes smaller with increasing radius of the conductor, so that dust, asperities, and water drops have a tendency to produce brushes all the more easily with bigger conductors. This explains in particular, the weak slope in the curves of losses of expanded cables observed by Pettee.

Tests on single, large diameter conductors are being pursued in France with the aim of determining the best type of conductor to be utilized for the equipment for future lines which are to be built for direct use at a voltage of 400 kilovolts without any preliminary utilization under 220 kilovolts. I shall be glad, in this respect, to know what results will be obtained at the station at Tidd.

In answer to the discussion by Bellaschi, I shall state that the comparison between a vertical and horizontal layout of bundle

conductors involves, on the one hand, the problems relating to the closing up of conductors and, on the other hand, the question of the cost of the line.

The first point is dealt with in Tables I and IV of my paper which point out an advantage of the vertical layout in avoiding the use of spacers for a span of 1,640 feet when the spacing between conductors is equal at least to 16 inches. However, since this report was written, we have been carrying out tests meant to render evident the risk of one wire twisting itself around the other following a sudden discharge of rime deposit. These tests have shown it was necessary to place at least one spacer in each span, even when using a vertical layout for the conductors; this of course lessens the interest of this latter disposition for areas exposed to the effects of hoarfrost.

With regard to the second point, a vertical layout costs more than the horizontal one owing to the fact that it requires slightly higher towers.

Pumping for Power in the Colorado Rockies

FRANK M. WILSON
ASSOCIATE AIEE

IT is the purpose of the paper to discuss features of the Granby pumping plant, which together with the Granby Dam and dikes, the Shadow Mountain Dam and dikes, and the Alva B. Adams Tunnel, constitute the major man-made facilities for delivering surplus water from the western slope of the Rocky Mountains to the eastern slope for irrigation and power development. The topography of the region makes possible the unique feature of pumping for power production on this project by utilizing for hydroelectric power generation the diversion water with its tremendous drop of approximately 2,973 feet as it flows down the eastern slope after it has been raised in elevation a maximum of 186 feet by the Granby pumping plant. This plant will require for pumping operation approximately six per cent of the electric energy which will ultimately be generated by the diversion water as it passes through seven power plants on its course down the mountains.

General Description of Project

The Colorado–Big Thompson transmountain water diversion project for irrigation and power development is being constructed in the heart of the Colorado Rocky Mountains by the Bureau of Reclamation. The main features of this project lie within a vast vacation area, and the project has been planned to preserve the fishing and recreational facilities of the region as well as the scenic attractions of Grand Lake and the Rocky Mountain National Park. This project provides for the annual diversion eastward through the Alva B. Adams Tunnel under the Continental Divide of 310,000 acre-feet of surplus water from the headwaters of the Colorado River. This water will be obtained from a drainage area of approximately 782 square miles varying in elevation up to 14,000 feet above sea level and lying west of the Continental Divide. Diversion of this water, which is the lifeblood of the arid west, was made possible by the Colorado River seven-state compact allocating 7,500,000 acre-feet annually to the upper basin states and by approval of the diversion to the eastern slope by the Western Slope Protective Association and by the Northern Colorado Water Conservancy District. The latter organization created under the laws of the state of Colorado has contracted to repay a part of the cost of the project.

The Green Mountain Dam, located on the Blue River west of the Continental Divide, is a link in this vast project and was built to protect the western slope water users against a possible water shortage due to diversion of Colorado River water to the eastern slope. Part of the total capacity of 154,060 acre-feet of water stored in the reservoir back of this dam will be used for the generation of electric power and part will be held in reserve to make up for any deficiency which may develop in the water users' normal supply as a result of the diversion. The Green Mountain power plant has an installed capacity of 21,600 kw and has an average annual energy generation of approximately 60,000,000 kilowatt-hours.

Granby Dam and four dikes will impound the water for diversion and will be of the earth and rock-fill type construction. The dam will have its crest at elevation 8290.5 and will have a width of 40 feet, a length of about 885 feet and a height above streambed of approximately 232 feet. The four dikes will each have a 30-foot crest width and their total crest length will be approximately 6,300 feet. The reservoir formed by the dam and dikes will have a surface area of 7,256 acres and a maximum storage capacity of 546,350 acre-feet of which 467,000 acre-feet is to be active storage. The water level in this reservoir will vary from a maximum elevation of 8,280 feet to a minimum elevation of 8,186 feet.

The Granby pumping plant which is being built on the north side of the Granby Reservoir about eight miles north of Granby, Colo., will raise the water from this reservoir up into Shadow Mountain Reservoir which will be connected by a short channel to an existing natural body of water, called Grand Lake.

Shadow Mountain Reservoir which will be formed by the Shadow Mountain Dam and dikes will add 1,356 acres to the 507 acre-surface area of Grand Lake. The water level in the reservoir and lake will vary from a maximum elevation of 8,367 feet to a minimum elevation of 8,366 feet. Since these bodies of water will be limited to a maximum variation in water level of one foot, they will provide only enough storage to correlate the pumping plant discharge with the required inflow to the tunnel. The water level in the Shadow Mountain Reservoir will be regulated by two 18-foot by 20-foot radial-type spillway gates which can be operated by either local manual pushbutton control, by automatic float control, or by remote manual control from the Granby pumping plant. Shadow Mountain Dam and dikes are of the earth and rock-fill type construction. The dam and dikes have a crest elevation of 8,375 feet and a crest length of approximately 3,100 feet. The dam has a height above the streambed of about 50 feet. For periods varying from one to as much as three months during the year the natural inflow into Shadow Mountain Reservoir and Grand Lake will supply all

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FRANK M. WILSON is with the Bureau of Reclamation, United States Department of the Interior, Denver, Colo.

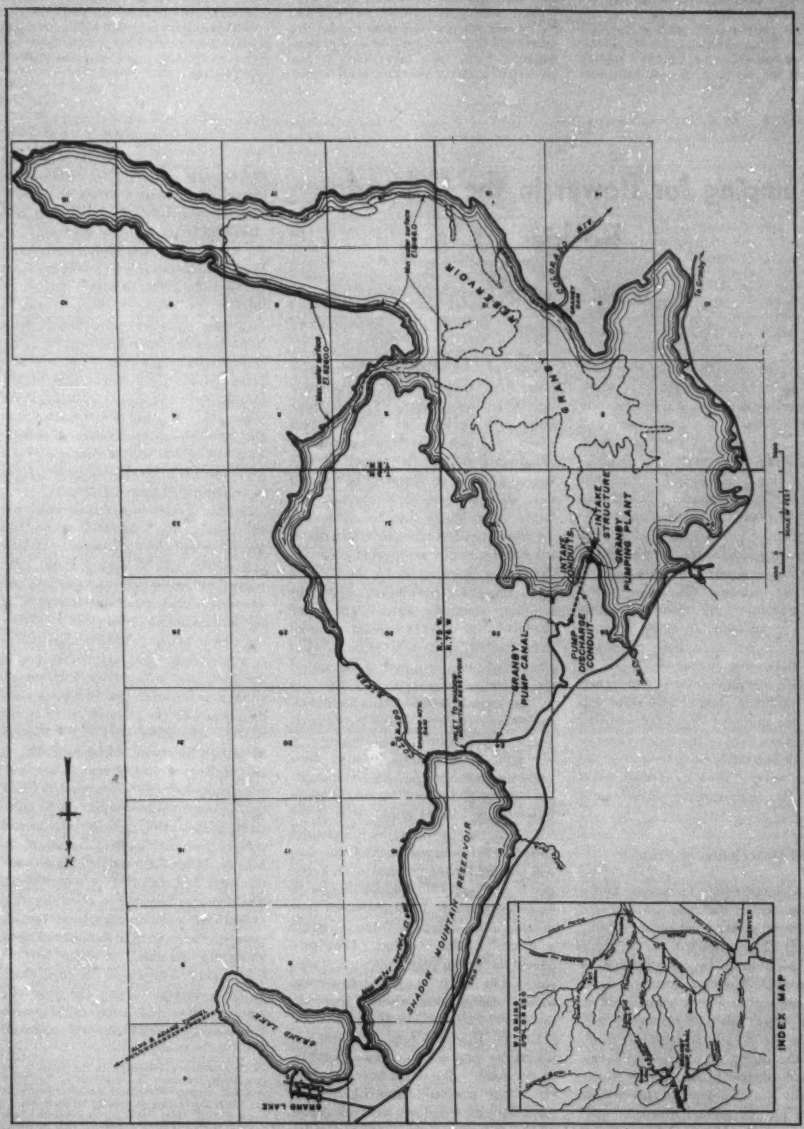

Figure 1. Location map of Granby Reservoir, Granby pumping plant, including the discharge conduit and canal, Shadow Mountain Reservoir, and Grand Lake

of the water required for diversion, and no pumping will be required. When the natural inflow exceeds the required diversion through the tunnel, the surplus water will be spilled into the Colorado River and stored in the Granby Reservoir. The possibility of using the pumping units for electric power generation during this period by diverting the surplus flow down through the pumps into the Granby Reservoir was considered. This plan was not economically justifiable, however, and was not included in the final design.

The 13.07-mile long Alva B. Adams Tunnel will carry the water by gravity flow from Grand Lake under the Continental Divide and Rocky Mountain National Park to the eastern slope. This tunnel, which is circular in section, has an inside diameter of 9.75 feet and has a capacity of 550 cubic feet per second. The west portal at the inlet end will take water from Grand Lake at a depth varying from 7.5 to 8.5 feet below the water surface in order to eliminate ice obstructions and to discourage entry of fish. The inflow to the tunnel will be regulated by two 9-foot by 10-foot radial-type gates which can be operated by either local manual pushbutton control, by automatic float control, or by remote manual control from the Granby pumping plant.

A location map of the Granby Reservoir, the Granby pumping plant including the discharge conduit and canal, the Shadow Mountain Reservoir, and Grand Lake is shown in Figure 1.

After leaving the east portal of the Alva B. Adams Tunnel, the water will pass through a series of siphons, conduits, tunnels, reservoirs, and a total of seven power plants on its way for use for irrigation purposes. Only a part of the diversion water will go through the last power plant in the series for release into the Big Thompson River. The seven power plants will have a total installed capacity of 154,300 kw and an estimated annual energy generation of 730,000,000 kilowatt-hours. Two of these power plants are now under construction, the Marys Lake power plant with a capacity of 8,100 kw and the Estes power plant with a capacity of 45,000 kw.

The developed profile and diagram of the project is shown in Figure 2.

Granby Pumping Plant

Building Design and Layout

The pumping plant is being constructed in a one to one slope excavation in sandstone approximately 115 feet deep. The pumping plant building will consist of a rectangular reinforced-concrete structure extending from 50 feet above to 14.5 feet below the finished ground level and resting on an open reinforced-concrete shaft, having a rectangular central section and semicircular ends, which extends approximately 117.5 feet to the bottom of the base slab. The lower structure will have interior cross-walls and beams and massive exterior buttresses for resisting the high external hydrostatic and earth pressures to which it will be subjected. The area around the pumphouse structure will be backfilled to elevation 8287.0 which is considerably higher than the original ground surface.

A ground mat of number 3/0 bare-copper cable covering an area of 5,460 square feet will be buried in the backfill over the intake pipes and connections will be brought to terminal boxes located in the walls of the pump floor. Bare-copper cable connections from these terminal boxes will ground all conduits, electric equipment, and exposed metal parts in the pumping plant and in the switchyard.

Six main floors will be provided in the plant in addition to the galleries for the intake and discharge valves. Four of these main floors will be below ground level and can be reached by both the stairs and the elevator. The semicircular longitudinal extensions of the building will house the elevator, stairs, cable, and pipe shaft, ventilating ducts, the drainage sump, and many of the plant accessories.

The gallery at elevation 8162.50 will provide access to the butterfly valves in the intake pipes and the gallery at elevation 8170 will provide access to the discharge valves, to the pump suction tubes, and to the end of the building under which the sump is located.

The floor at elevation 8186.25 will provide access to the main pumps and on it will be mounted the discharge valve control mechanisms, two sump pumps, the miscellaneous power distribution board, the unit auxiliaries power boards, the unit hydraulic gauge and control boards and other miscellaneous auxiliaries. The pumping units can be operated from this floor.

The main pump motors, the motor excitation-control cubicles, and the distribution transformers with secondary resistors for grounding the motor neutrals will be located on the floor at elevation 8211.25.

The intermediate floor at elevation 8252.50, which will provide storage space was built primarily for bracing the outside walls at this elevation.

The cable spreading room, the air compressor room, the oil purifier room, and the oil storage room are all located on the elevation 8272.50 floor. The switchyard transformers will be set down on this floor from above through a hatch for untanking operations. The two unwatering pumps for the plant, each with a capacity of 4,500 gallons per minute, will be mounted on a platform at elevation 8278.0 and will be accessible from this floor. These pumps will be used should the plant become flooded, and they can be used, if necessary, to supplement or replace the sump pumps in normal plant operation.

The service bay for the plant is to be located in the southeast end of the building extending from the ground-floor at elevation 8287.5 to the roof. This section of the building will provide unloading space for the equipment and also space for the storage of parts during the assembly and disassembly of the pumping units and transformers. The machine shop equipment will also be located on the ground-floor in this section of the building. Provision will also be made in this part of the building for a future emergency gasoline engine driven generator set to take care of the essential plant auxiliaries should all outside sources of power fail. The control room containing the motor-control switchgear, the line-control board, the supervisory control board, the telephone equipment, and the intake valve control board is also to be located on the ground-floor. The remainder of this floor will be occupied by washrooms, toilets, and miscellaneous rooms.

The electrical laboratory, the battery room, the ventilating equipment room, and the station-service room containing the unit substation switchgear and the miscellaneous power, heating, lighting, and d-c board will be located above the control room on the floor at elevation 8302.50.

The pumping plant will be serviced by three cranes for handling the equipment. The main 50-ton crane for the plant will be located in the service bay and will be used to unload equipment in the plant, to untank the switchyard transformers and to handle parts for the pumps, motors, valves, and other equipment through hatches in the subterranean structure down to the motor floor. The 25-ton crane above the motor floor will be used in assembling and dismantling the motors and to handle valve and pump parts and other equipment below the motor floor. The crane above the pump floor will be used for assembling and dismantling the pumps.

The building will be heated by electric space heaters with an installed capacity of about 390 kw and the ventilating sys-

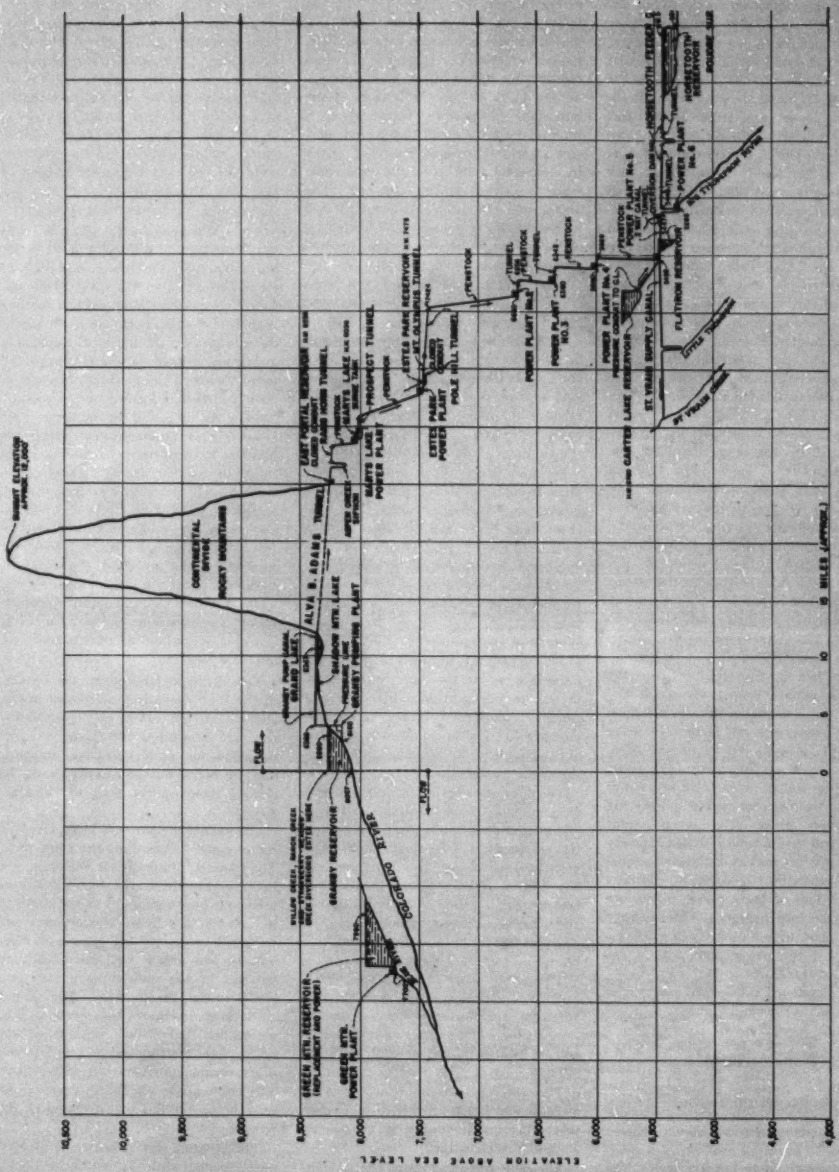

Figure 2. Developed profile of the Colorado—Big Thompson Project showing power drop of diversion water on the eastern slope

tem has been designed to circulate approximately 20,000 cubic feet per minute of air through the lower part of the structure to keep dampness to a minimum.

A longitudinal section through the pumping plant is shown on Figure 3.

The general plan and profile of the pumping plant is shown on Figure 4.

Hydraulic Features

Pumps

Three vertical-shaft, centrifugal, single-stage pumps, each with a capacity of 200 cubic feet per second at the maximum pumping head of 186 feet, will be installed in the pumping plant. The 94-foot variation in the inlet head in the Granby Reservoir together with the long suction pipes and the low barometric pressure at this altitude made it necessary that the pumps be installed below the minimum reservoir level to insure freedom from cavitation throughout the wide operating range of head and discharge. As a result the centerline of the pumps will be located at elevation 8,180 which is six feet below the minimum reservoir level, and 107.5 feet below the ground-floor of the plant.

Intake Structure and Pipes

Each pump will be supplied by an 87-inch inside diameter concrete pipe approximately 570 feet long running from the intake structure located in the Granby Reservoir. Each suction pipe will be provided with an 88-inch butterfly valve located within the pumping plant and operated from the control room which is above the high water level of the reservoir. This valve will be closed when unwatering the pump for inspection and repair.

The radial-type reinforced concrete intake structure will be 40.5 feet high and will have its floor 15.5 feet below minimum reservoir level. The structure will have six radial openings with steel trashracks for the entrance of water. Provision is made to shut off the entrance of each of the three intake pipes by vertical slide bulkhead gates. A bulkhead gate will be closed only when it is necessary to repair a butterfly valve in an inlet pipe.

Discharge Valves and Pipes

A water hammer analysis has shown that the pumping plant will operate satisfactorily without a surge tank connected to the discharge lines and that water column separation will not occur in the suction or discharge lines under normal or emergency operating conditions. A minimum pressure of 14 feet below atmospheric pressure will be reached momentarily on the discharge line under emergency conditions when power failure occurs simultaneously on all three pump motors. A maximum pressure of 210 feet on the discharge line will be obtained under emergency conditions after simultaneous power failure on all three pump motors.

Each pump will discharge water through a steel pipe provided with a 76-inch butterfly valve. The butterfly valves will each be operated by hydraulic servomotors which receive oil from a central oil pressure system. For both normal and emergency operation the valves will be controlled to close $67^1/_2$ degrees in 12 seconds and the remaining $22^1/_2$ degrees in 30 seconds. The opening cycle will be completed in 16 seconds.

The steel discharge pipes will enlarge to 87 inches inside diameter beyond the butterfly valves and will be encased in concrete to the point beyond the plant where the branch connections from the individual pumps merge into a single reinforced concrete conduit having an inside diameter of 11 feet. The 11-foot diameter conduit will be approximately 3,413 feet long and will discharge through a siphon and over an apron into the Granby Pump Canal which will carry the water to the Shadow Mountain Reservoir. The invert at the crest of the siphon is to be at elevation 8369.0. An air-vent in the top of the siphon, interlocked with the motor circuit breaker, will prevent backflow from the canal. A float-controlled vacuum pump will be used to remove the air pocket at the top of the siphon and to insure full and efficient operation of the siphon during pumping operations.

Granby Pump Canal

This canal will carry the water from the conduit discharge to the Shadow Mountain Reservoir and will be about 9,380 feet long with a constant grade of 0.000146 and a capacity of 1,100 cubic feet per second. Most of the canal is being excavated in cuts, the maximum being about 68 feet. The canal discharge structure has two 12-foot by 12-foot radial-type gates which have the concave surface toward the reservoir to prevent return flow into the canal and to permit drainage of the canal for maintenance and inspection.

Electrical Design

Power Supply and Switchyard

The pumping plant through its adjacent switchyard will be connected by a 69-kv transmission line approximately 42 miles long to the Green Mountain power plant and by a transmission circuit of either 115 or 69 kv to the Estes power plant where it will be tied in to the power system on the eastern slope. Since operation of the eastern slope power plants is so closely tied in with pumping operations, reliability of power supply is very important. For this reason the pumping plant has been designed for two methods of motor starting, namely: Full-voltage starting when connected to the normal power system, and reduced-voltage starting when the transmission circuit to the eastern slope is out of service and the plant is connected to the minimum system through the 69-kv transmission line to the Green Mountain power plant.

The switching diagram for the Granby pumping plant is shown by Figure 5.

If the 115-kv circuit from the Estes power plant is selected, it will be tied to the 69-kv line from Green Mountain by three single-phase 7,500-kva delta-delta connected transformers located in the Granby switchyard.

The three 69-kv 600-ampere 3-pole single-throw outdoor-type power circuit-breakers in the switchyard will each have an interrupting rating of 500,000 kva at rated voltage and frequency.

Each motor in the pumping plant will be supplied by a 3-phase 5,750-kva 66- to 6.9-kv delta-wye connected, oil-immersed, self-cooled, outdoor-type transformer located in the switchyard. The high-voltage windings will have $2^1/_2$ and 5 per cent full capacity taps both above and below 66 kv. Reduced voltage for motor starting on the minimum system will be obtained by taps in the Y-connected low-voltage windings of the transformers. Three taps will be provided in each low-voltage winding to give reduced voltages of 30, 40, and 50 per cent of normal voltage. The Y-connected windings will give balanced voltages and currents for motor starting for whichever tap is determined by test in the field to be most suitable for the operating conditions. A phase displacement will not be involved in the motor transition from reduced to full voltage when the transformer low-voltage windings are wye-connected.

Full-voltage starting of the first motor will cause voltage dips on the 69-kv bus at Granby varying from 10.1 to 20.6 per cent of normal system voltage depending on the connected system, the type of transmission circuit to the Estes power plant which is selected and the initial voltages at the Estes and Green Mountain power plants.

Starting the first motor on the 40 per cent voltage transformer tap when con-

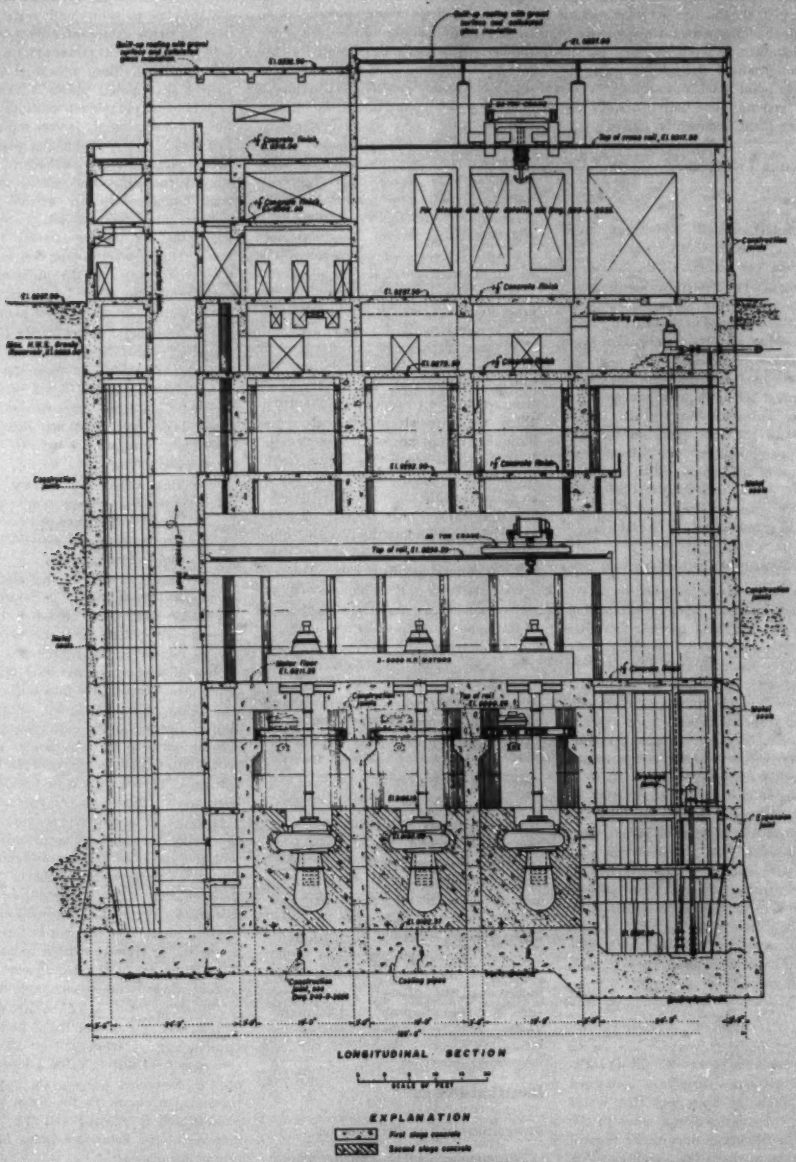

Figure 3. Longitudinal section through the Granby pumping plant

nected to the minimum system will cause a voltage dip on the 69-kv bus at Granby of approximately 10 per cent.

Motors

The three vertical-shaft synchronous motors driving the main pumps will each be rated at 6,000-horsepower 3-phase 60-cycle 327-rpm 6,600-volt and 0.95-power factor leading. The motors will rotate in a clockwise direction and will be designed to withstand the stresses resulting from operation at a runaway speed of 440 rpm.

The system constants under which the main pump motors will be designed to operate are as follows:

For the condition of reduced-voltage starting and synchronizing on the minimum system:

$$Z_s = R_s + jX_s = 0.16 + j0.90$$
$$Z_g = R_g + jX_g = 0.16 + j1.27$$

For the condition of full-voltage starting and synchronizing on the normal system:

$$Z_s = R_s + jX_s = 0.026 + j0.63$$
$$Z_g = R_g + jX_g = 0.026 + j0.74$$

Where Z_s is the external impedance from the motor terminals to the point of equivalent minimum system regulated voltage and Z_g is the external impedance from the motor terminals to the point back of transient reactance of minimum system synchronous machine capacity with all quantities expressed as per unit on a 25,000-kva base.

Each motor will be designed to start on the minimum system with 36.8 per cent of normal voltage across its terminals and an initial inrush of 4,000 kva when the pump discharge valve is closed, the water level in the suction tube is below the pump impeller, and the thrust bearing friction is reduced by pressure introduction of an oil film between the bearing surfaces. Each motor will also be designed to start on full voltage on the normal system with an initial inrush of 29,400 kva when the valve in the discharge pipe is closed, the pump casing is full of water and an oil film has been established between the thrust bearing surfaces. Each motor will have a synchronous condenser capacity of 2,200 kva when operating at rated voltage, leading power factor, and no load, with the discharge valve closed and the water level in the suction tube below the pump impeller. The motors will have a pull-out torque at rated voltage, frequency and field current of not less than 200 per cent of full-load torque.

The motors will each have a spring-type thrust bearing and a guide bearing above the rotor and a guide bearing below the rotor. A thermostat relay bulb will be embedded in the metal of the thrust bearing and of each guide bearing to operate an alarm on excessive bearing temperatures. Two resistance temperature detectors will be embedded in the thrust bearing metal of each motor for indication at the motor-control switchgear of the bearing temperature. A motor-driven oil pump mounted within the motor air housing will provide oil pressure lift to the thrust bearing during the starting operation. The thrust bearing will transmit its load, composed of the weight of the rotating parts of the motor and pump, plus the unbalanced hydraulic thrust, through a bearing bracket to the stator frame.

The motor armature windings are to be insulated with class *B* insulation, and the coils will have corona shielding and will be treated to prevent permanent injury from temporary exposure to dampness. Electric heaters with a capacity of 15 kw in each machine will be used to prevent condensation in the windings when the motor is not operating. Embedded resistance temperature detectors will give indication of armature winding temperatures at the motor-control switchgear. The armature windings of each motor will be Y-connected with three main leads and three neutral leads brought out of the stator frame. These windings are to be protected by differential current relays. The main leads will be connected to surge-protective capacitors and lightning arresters mounted within the motor housing. The neutral leads will be grounded through a distribution transformer having a secondary short-circuiting resistor. The field winding of each motor will also be insulated with class *B* insulation, and the field poles will have amortisseur windings adequate for starting, accelerating, and synchronizing the motor under the required operating conditions.

Each motor will have an enclosed ventilating system with a water-cooled surface air-cooler at one end. All cool air from the surface cooler will enter the machine from the top, and fans on the rotor will provide the necessary circulation. The complete cooler will be handled by the overhead crane through an opening in the air housing coverplate. A removable coverplate on one side of the air housing will provide access to the cooler for cleaning and removing tubes. The motors are being designed so that the maximum temperature rise of both the armature and field windings will not exceed 60 degrees centigrade when delivering rated output continuously with rated voltage, power factor, and frequency and with cooling air entering the motor at not more than 40 degrees centigrade. The cooling water temperature will not exceed 25 degrees centigrade as it enters the cooler. The thermal capacity and cooling system of each motor will be adequate to permit two complete consecutive starts at either reduced or full-voltage with a third start 10 minutes after the second start.

A corrosion-resistant metal ringheader having a minimum bursting pressure of 6,000 pounds per square inch and provided with discharge nozzles will be installed above the rotor for the carbon-dioxide fire-protection system. A pressure-relief door will be provided to prevent distortion of the air housing due to internal pressures caused by the release of the carbon dioxide gas.

Compressed air operated brakes furnished with each motor will be capable of bringing the motor and pump rotating parts to a stop from one-half speed within five minutes after application, without field excitation on the motor. The brakes will also serve as hydraulic jacks to lift the motor and pump parts for removal or adjustment of the thrust bearing and for removal of the pump intermediate shaft from below. The lifting jacks will be operated by a hand-operated high-pressure oil pump. Removal of the pump intermediate shaft will permit dismantling the pump without disassembling the motor. Each motor will be equipped with a direct-connected, d-c, vertical-shaft, shunt-wound, main exciter. Automatic voltage regulation will be accomplished by means of a regulator using a separate motor-driven amplidyne generator. This is to be one of the first applications of this type of regulator to slowspeed hydroelectric machines.

Control and Operation

Normal operation will be from the main control room on the ground level floor, but the pumping units may be operated from either the switchgear in the control room or from the unit hydraulic gauge and control boards located on the pump floor.

The supervisory control board located in the control room will provide recording, indication, telemetering, and control of remote features related to pumping and storage operations as follows:

1. Continuous recording of water levels in the tunnel, in Shadow Mountain Reservoir, in Granby Reservoir, and in the channel carrying overflow from Granby Reservoir.

2. Indication and telemetering of gate positions at the west portal of the tunnel and at Shadow Mountain Spillway.

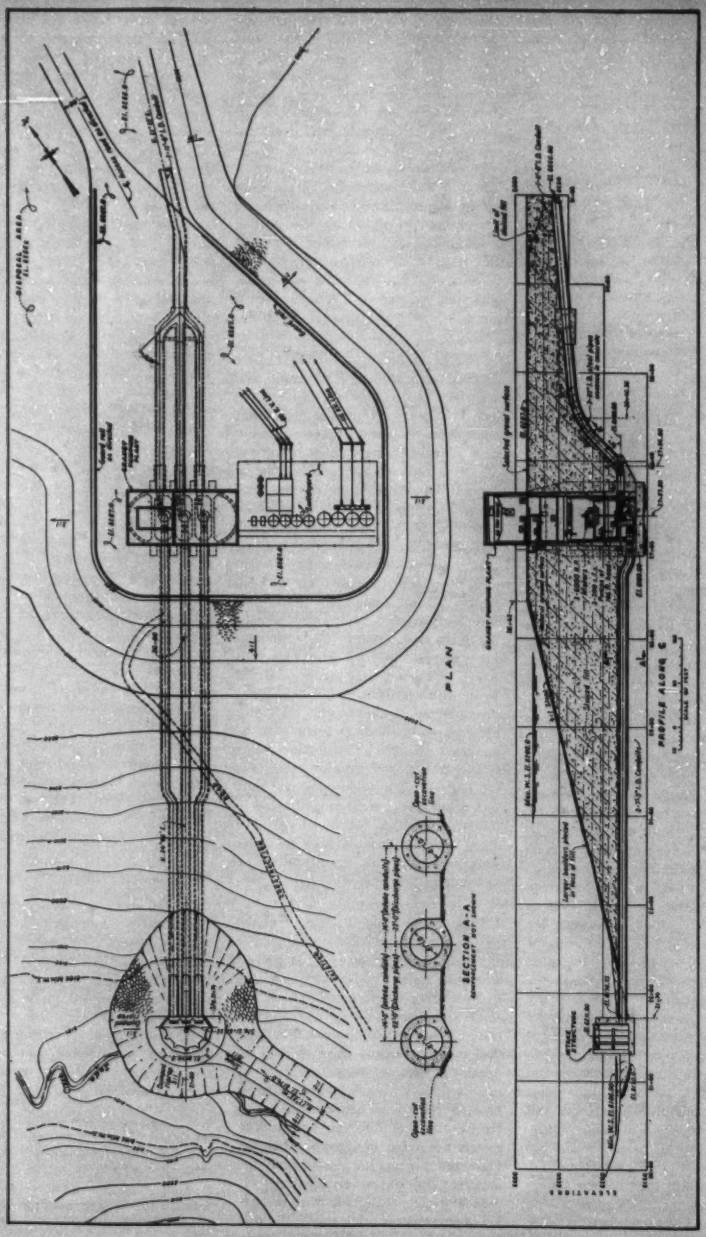

Figure 4. General plan and profile of the Granby pumping plant, including the intake structure, the suction pipes, and the discharge pipes

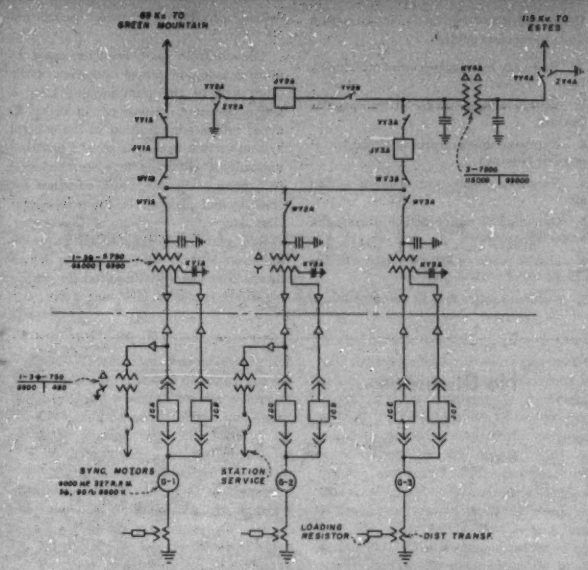

Figure 5. Switching diagram for the Granby pumping plant

3. Remote control of the gates at the west portal of the tunnel and at the Shadow Mountain Spillway to place them under either automatic float control or remote manual control.

The line-control board in the control room will provide the control, protection, and instrumentation for the switchyard equipment and the transmission lines.

The motor-control switchgear in the control room will provide the control, metering, instrumentation, and protection of the three main pump motors and the control and indication of auxiliaries including the discharge valves. This switchgear is to be of the metal-clad type with removable circuit breakers and will be of the 15-kv class due to the high altitude of the installation. The reduced-voltage and the full-voltage circuit breakers in the switchgear will both have a continuous current rating of 600 amperes and an interrupting rating of 150,000 kva at 15 kv.

Selection of the full-voltage or the reduced-voltage motor starting procedure will be determined by the operator based upon the connected power system and will be made by the manual operation of a selector switch on the motor-control switchgear. With the pump discharge valve in the closed position, the next step will be the starting of the pumping unit auxiliaries, including the voltage-regulator motor-generator set, from either of the two control stations. Both methods of motor starting will involve pressure introduction of an oil film between the motor thrust bearing surfaces to lower the necessary starting torque of the motor. The reduced-voltage starting method will require, in addition, that the water level in the suction tube be depressed by compressed air below the pump impeller. After these preliminary operations, the motor may be started by the manual operation of a control switch at either of the two operating stations. Interlocking in the motor control equipment will prevent the motor from starting unless the unit auxiliaries are functioning properly and the following conditions exist:

1. The thrust bearing oil pump produces oil pressure.
2. The pump guide bearing oil pump produces oil pressure.
3. The pump discharge valve is fully closed.
4. Water flows to the motor air-cooler and the thrust bearing cooler.
5. Cooling water flows to the pump stuffing box.
6. The pump casing is unwatered (for reduced-voltage starting only).
7. Water flows to the pump seal rings (for reduced-voltage starting only).

When the selector switch is in the position for reduced-voltage starting, operation of either motor-control switch will close the reduced-voltage circuit-breaker in the switchgear applying reduced voltage to the motor which will start and accelerate to maximum subsynchronous speed as an induction motor. The motor will be synchronized at reduced voltage by the automatic application of excitation to the motor field at the optimum conditions of speed and rotor pole angular displacement. The reduced-voltage circuit breaker will then be opened automatically, and the full-voltage circuit breaker will be closed automatically in an open circuit transition to apply full voltage to the motor. Manual operation of a control switch will then introduce water into the pump casing until rated pressure at zero discharge is attained. The pump discharge valve will then be opened by operation of a control switch and actual pumping will begin.

The full-voltage starting sequence will be similar except the motor will be thrown on full voltage for starting and synchronizing, and the pump casing will be full of water during the operation.

A motor will be automatically shut down by any one of the following conditions:

1. Incomplete starting sequence.
2. Loss of motor field excitation, or loss of synchronism.
3. Unit overspeed.
4. Excessive pump casing water pressure.
5. Motor thermal overload relay operation.
6. Motor overcurrent relay operation.
7. Motor undervoltage relay operation.
8. Motor differential current relay operation.
9. Motor ground fault relay operation.

Carbon dioxide for extinguishing fire will be automatically released into a motor air housing upon operation of the differential current or ground fault relays for the motor or by operation of the thermal links in the air passage ahead of the motor cooler. Carbon-dioxide manual release stations will be provided on the motor-control switchgear and at the motor floor.

Provision will be made in the control equipment that tripping a motor off the line for any cause, including normal shutdown, will also perform the following functions:

1. Close the pump discharge valve.
2. De-energize the motor field contro land

voltage-regulator equipment including the motor field circuit-breaker and the exciter field contactor.

3. Apply the compressed air operated brakes by means of a speed switch on the motor after the motor has decelerated to a low speed.

4. Close a contactor energizing the motor heaters.

For normal shutdown, the pump discharge valve will be closed before the motor is tripped off the line to minimize system disturbances.

The following conditions will not automatically shut down a pumping unit, but the annunciator system will call them to the operator's attention for investigation and appropriate action:

1. Excessive temperature of pump stuffing box.

2. Excessive temperature of pump guide bearing.

3. Excessive temperatures of motor thrust or guide bearings.

4. Excessive temperatures of motor-cooling air.

5. Operation of motor carbon dioxide fire protection system.

6. Loss of power or low oil-pressure in discharge valve control system.

7. Failure of water flow to pump seal rings (when operating with pump casing unwatered).

The controls have been arranged to allow reasonably simple operation of the plant and of the pumping units by a single operator. However, in order to avoid undue complication in the control system certain functions will be initiated manually which could have been included in a completely automatic sequence for starting and placing the units in operation.

It is planned to operate the pumping units on system off-peak power which means that the units required for pumping will be started at least once a day.

No Discussion

The Space Charge Due to Corona

OTTO J. M. SMITH
MEMBER AIEE

A NEW technique for measuring the field strength and deducing the ion density about a wire in corona is needed. The research described herein was aimed at developing one such new method. A better understanding of the physics of corona and ion recombination can follow the application of new experimental techniques to confirmatory investigations of a-c corona phenomena.

The use of increasingly higher voltages in electrical engineering has brought the problem of corona and its effect upon the design and operation of equipment and transmission lines into a position of singular importance in research. It is desirable for an operating engineer to know the power loss due to corona on a line under various conditions, and for the design engineer to be able to predict the characteristics of a proposed line. Each must rely heavily upon empirical relationships. The continually accumulating data from research will eventually result in a more complete theory and less need for reliance upon empirical tests.

In this work, the field strength about a wire with a-c corona was measured directly with a crystal galvanometer connected between two probe screens. The net space charge density was derived from the field strength curves in a single mathematical step, using Poisson's equation. The experimental curves show clearly the mechanism of the ion motion, and their form is easily derived artificially from some simple hypotheses.

The position and motion of the ions about a wire in corona is the physical manifestation of the mechanism of corona. If the ion densities and motions are known completely, one can determine mathematically the less concrete field strength and voltage distributions. Unfortunately, the ion densities cannot be measured directly. In previous studies of corona mechanism, attempts were made to obtain the ion densities from measurements of the potential at different positions in space. This was inherently a difficult method. The field strength was proportional to the derivative of the potential with respect to the radius. The ion density was equal to the difference between two large numbers, one a function of the second derivative of the potential, and the other a function of the first derivative. Multitudinous values of potential had to be determined for small radius increments in order to have meaningful second derivative figures. Even so, small percentage errors in potential could cause large percentage errors in ion density.

If the original data are direct measurements of field strength, then one need compute only a function of the difference between points on the original curve and the slope of this curve. The amount of laboratory work required is reduced by one order of magnitude.

The previous investigators in this field used oscillograms of voltage and corona current,[1] electrostatic voltmeters with probes,[2-6] and rotary generating electrostatic voltmeters.[6,7] The determinations of space charge potentials with a single probe or exploring wire may be in error due to the inherent limitations of probe measurements. These have been conveniently enumerated by Loeb.[8] The primary assumption in all probe measurements is that the presence of the probe does not alter either the potential distribution or the charge distribution in the region to be studied. Even without a space charge being present, a potential determination with a probe is unsatisfactory, for the indicating instrument must have a very low capacity and a very high resistance. The probe potential will be changed by the admittance of the instrument, and leakage conductance might give a time variation of the potential that would be most annoying. In the presence of space charge, a floating probe will attain a potential so that the fastest moving ions will be repelled, or so that the net current from bombarding positive and negative ions will be zero. Thus, even if the measuring instrument connected to the probe takes no current, the probe will alter the potential distribution if the ion velocities are appreciable. If the current requirements of the measuring instrument are large, the charge density in the vicinity of the probe may be lowered. In this case, the probe potential will be determined by the net flow of charge to the probe and the resistance of the measuring instrument. There is always doubt concerning the actual potential which the space would have were the probe not there.

The mechanism of corona loss is most closely associated with ion movements and densities. This is what one would like to measure experimentally, but can not. They can be partially computed from the field strength using Poisson's equation. This relationship is derived in the Appendix. It is possible to compute the field strength from the potential distribution, but to get useful ion information from measured potentials via two successive computations is beyond practicality. One would prefer, then, to obtain his experimental data in the form of field strength curves.

W. G. Hoover[6] used two probes to measure the field strength about a wire in corona. This method reduced the inherent errors found in single probe measurements. The two probes were close enough together that the errors in the floating potentials of the two due to

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Otto J. M. Smith is with the University of California, Berkeley, Calif.

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Figure 1. Complete assembly showing the operating bench nine feet above the floor

Note the oscillograph, motor, and arc light on the swinging bench, and the cam switch on the table

bombarding ions were approximately the same, and therefore did not enter into the measured voltage between the probes. However, the change in floating potential due to ion collection altered the potential distribution slightly, and this indirectly slightly affected the field strength. The energy for the a-c galvanometer connected between the probes was derived from rotating the probe assembly as an electrostatic generating voltmeter, and this reduced to a minimum the drain on the space charge.

The author followed the practice of many of the previous investigators in using the familiar physical arrangement of a wire on the axis of a cylinder to confine the electrical field. This has many advantages over other configurations. The field is radial and symmetrical, and lends itself readily to mathematical treatment. The space inside of the cylinder is comparatively free from external fields and influences, and the dimensions can be chosen to simulate most desired conditions. It has the further practical advantage that very little equipment needs to be insulated for a high potential to ground. Figure 1 shows the test equipment.

The field strength was measured as the difference in potential between two probes. As mentioned before, the errors were due only to second order effects. The field strength error due to the accumulation of charges by the probes was zero near the half voltage radius, and approached the same percentage as the potential error near either the inner wire or the outer cylinder. The error was proportional to the ion density, and was always in the direction of indicating larger ion densities than actually existed. The results were not, therefore, in disagreement with fundamentals but only exaggerated the phenomena we wished to examine in the near vicinity of the inner wire or the outer cylinder. The field strength which was measured was the resultant value due to all of the contributing parts of the electric system, and included that due to charges in space, the potential between the wire and the cylinder, and charges on the wire and cylinder. It is this value which is needed for Poisson's equation.

Theory

Universal Field Strength Curve

The field strength is inversely proportional to the radial distance from the central wire. This is a hyperbolic distribution, and would plot as a straight line with a slope of Er equals $V/(\ln r_2/r_1)$ on a linear graph with co-ordinates of E and $1/r$. This is equation 20, Appendix I. There would be a family of straight lines, one for each voltage, each going through the origin. A vertical voltage scale for these lines could be constructed with the same values as the field strength scale, on the abscissa $1/r = \ln r_2/r_1$. Since V/E equals $r \ln r_2/r_1$, a universal curve for a given physical arrangement can be plotted as a single straight line on coordinates of V/E and r, with a slope of $\ln r_2/r_1$. This is shown in Figure 2.

If there is a charge present, it will cause the field strength curve to vary from its usual hyperbolic form. If plotted with the co-ordinates of Figure 2, those regions

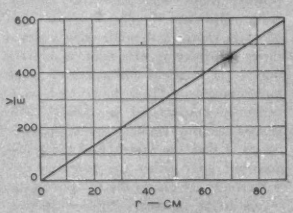


Figure 2. Universal field strength curve for no space charge

The ordinate is the ratio of applied voltage to field strength in units of volts per volt per centimeter. The abscissa is the radius in centimeters. The slope is drawn for a center wire r_1 of 0.1295 centimeter and an outer r_2 of 91.5 centimeters

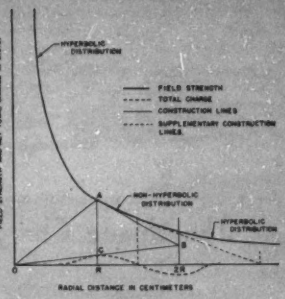


Figure 3. Graphical construction of total charge curve

The abscissa is the radial distance in centimeters, and the ordinate is the field strength and the net total charge in consistent units

with no charge would be represented by straight line sections, which if extrapolated, would go through the origin. Other connecting curve sections would indicate the presence of ions. The charge density is given by equation 17 in the Appendix

$$4\pi\rho = \frac{dE}{dr} + \frac{E}{r} \tag{17}$$

The density can be calculated from a field strength versus radial distance curve. dE/dr is the slope of this curve, which is negative. E/r is the slope of the line from the origin to the point on the curve under consideration; it is positive, and almost equal to $-dE/dr$. The numerical difference between these slopes in statvolts per centimeter divided by 4π is the charge density ρ in electrostatic units per cubic centimeter.

Total Charge

The amount of charge present may be shown quantitatively by using a special function of the charge density. This is the total net charge contained in a cylindrical shell, one centimeter long, one centimeter thick, and of a parametric radius r. This total net charge is designated by Q. It is a convenient notation, for Q does not vary as a cloud of ions moves radially, but can only change by diffusion, recombination, or the introduction of new ions.

$$Q = 2\pi\rho r = {}^1/_2(E + r\,dE/dr) \tag{22}$$

The area under a curve of Q versus r is the total net charge contained in the entire electric field per centimeter axial length of the cylinder. The area under the curve of Q between two co-ordinates r' and r''

is the total net charge per unit axial length in the cylindrical shell of internal radius r' and external radius r."

The total charge can be determined graphically from the curve of field strength versus distance. The construction is shown in Figure 3. The line AB is drawn tangent to the field-strength curve at the point A, which corresponds to the parametric radius R. This line has a slope of dE/dr. It intersects the ordinate line for the radius $2R$ at the point B. The vertical distance between A and B is equal to the term $r\,dE/dr$ in equation 22. The vertical distance from R to A is equal to E in electrostatic units. Since the slope is negative, $(E+rdE/dr)$ is equal to R to A to B, or the distance from point $2R$ to B. Since Q is one half of this value, a line is drawn from the origin to the point B, intersecting the ordinate RA at the point C. RC is the total net charge, Q, expressed in electrostatic units if the field strength E is measured in electrostatic units.

Experimental Procedure

Equipment

The large cylinder used was 12-feet long, 6-feet in diameter, vertical axis, bottom 3 feet above the ground, and made of 1/2-inch mesh galvanized iron wire screen. See Figures 1 and 4. The central wire was number 10 copper, 0.102-inch in diameter, which was connected to a 60-cycle variable voltage supply of 150,000 volts, 75 kva. The dimensions of the wire and cylinder gave a critical corona voltage of 48,500 volts peak or 34,300 volts effective between the wire and the cylinder.

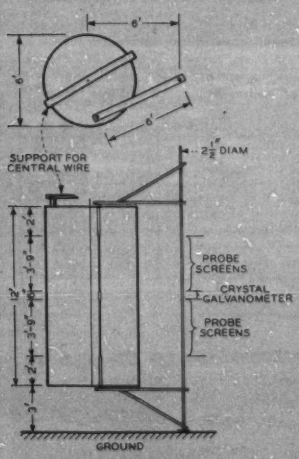


Figure 4. Dimensions of the large cylinder and the probe screens

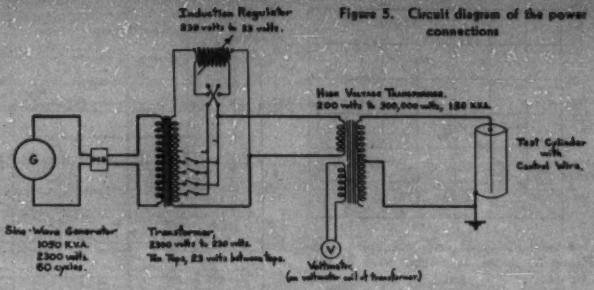


Figure 5. Circuit diagram of the power connections

The probe screens used to explore the field were two flat strips of 1/8-inch mesh galvanized iron-wire screen, placed inside of the large cylinder and parallel to the axis. The strips were 3/4-inch wide and $7^1/_2$-feet long, parallel to each other and spaced 0.5 centimeter apart. They were oriented with their flat sides perpendicular to a radius vector, so that they were occupying positions which were equipotential surfaces before their introduction. The screens were insulated electrically and mechanically from both the central wire and the cylinder, and yet movable, so that they could be placed at any desired radial distance. They were suspended vertically between the free ends of two horizontal booms, one above and one below the cylinder, which were carefully isolated and damped for 120-cycle and low-frequency building vibrations. Screens were chosen for probes in order to allow the free passage of ions. A two-foot guard ring was left at each end of the large cylinder to insure uniformity of the field.

The power connection diagram is given in Figure 5. The largest harmonic in the generated voltage was the third, and this had a magnitude of only 0.36 per cent. The peak voltage on the high-voltage secondary of the transformer, therefore, could be accurately computed from the measured effective voltage on the voltmeter coil.

The field strength measuring instrument was a commercially available crystal galvanometer mounted between the probe screens. The crystal was a bimorph, in which two flat rochelle salt crystals 2-inches long and 1/8-inch wide were cemented together with their crystallographic axes in opposite directions. One end was rigidly clamped, and the other carried a 1/8-inch square mirror. The whole was in a chamber of damping oil. Between the crystals was one electrode and each exposed face carried an electrode which was connected to the other terminal. A voltage applied between the two terminals caused a twisting motion which depended upon the relation of the longitudinal dimension of the plate to the crystalline axes. The deflection in radians was approximately proportional to the charge on the electrodes. The capacity of the crystal, however, varied with the temperature, so that the response of the crystal in radians per volt was a function of the temperature. The measurements were all made at constant temperature, so that no corrections were necessary for this work. Other crystal data were:

1. Maximum safe deflection, 0.05 radian
2. Minimum sensitivity, 0.0002 radian per volt (at maximum deflection).
3. Resonant frequency, 7,000 cycles per second.
4. Leakage resistance, over 100 megohms.
5. Capacity, 2,000 micromicrofarads.

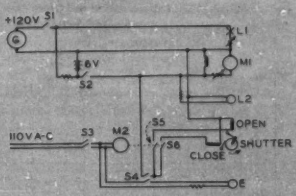


Figure 6. Circuit diagram of the oscillograph

G is a 120-volt d-c generator. L1 is an arc light. M1 is a variable-speed, 6-volt, 1250-rpm, film-drum motor. L2 is the oscillograph internal light. M2 is a 120-volt 60-cycle synchronous motor driving the cam switches S5 and S6. The oscillograph shutter is spring actuated and solenoid tripped, turning in the direction of the arrow. E is a standard oscillograph element for the 60-cycle applied voltage timing wave

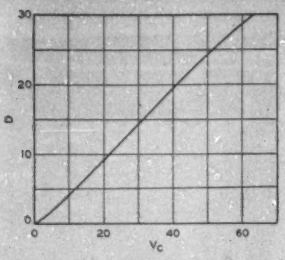


Figure 7. Rough calibration curve of the crystal

D is the peak beam deflection in millimeters on the film. V_c is the peak applied potential in volts, 60-cycles

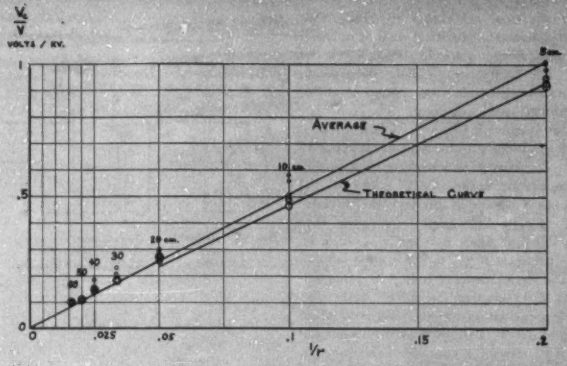


Figure 9. Crystal assembly rough position calibration

The experimental points and the theoretical curve are the same values as Figure 8, with both co-ordinates divided by V. The spreading of the points at 10 centimeters is due to insufficient accuracy of the crystal calibration

The light source for the crystal galvanometer was a razor blade slit upon which was focused the light from a d-c arc lamp. The slit was located outside of the cylindrical cage, and three feet from the galvanometer. The light from the arc slit was reflected from the crystal mirror back through a lens mounted in front of the mirror.

It then passed through a hole cut in the side of a standard type of magnetic oscillograph, through a shutter and collimating lens, onto a moving film. The dimensions of the optical system were: 20 inches from the arc light to the slit, 39 inches from the slit to the crystal galvanometer mirror, 46 inches from the mirror to the collimating lens, and one inch from the lens to the film.

The mirror was very small and light, and had some unavoidable imperfections. They were not important for short optical systems, but were very noticeable in the long system necessary for this work. Most of the mirror was therefore covered and shielded from the light, with only a portion having a single focal length being used.

A timing wave was obtained on the same film with the curve of field strength. This was produced by a magnetic oscillograph element, supplied from a 60-cycle voltage source of the same phase as the high-voltage applied to the wire in corona. The film drum speed was 240 rpm of a 16-inch circumference drum.

The oscillograph, film-drum motor, arc lamp, and light slit were all mounted on a swinging platform nine feet above the ground. The platform was built to rotate concentrically about the same axis as the swinging booms which supported the probe screens and the galvanometer. The platform was designed not to touch either the boom supports or the cage supports, consequently a special type of mounting was employed.

Figure 6 shows the oscillograph circuit

Figure 8. Crystal assembly voltage calibration

The experimental values of V_c are determined from film measurements of D and Figure 7. The theoretical curve was computed from capacity measurements and the applied high voltage. The deviation from a straight line indicates insufficient accuracy in the rough crystal calibration

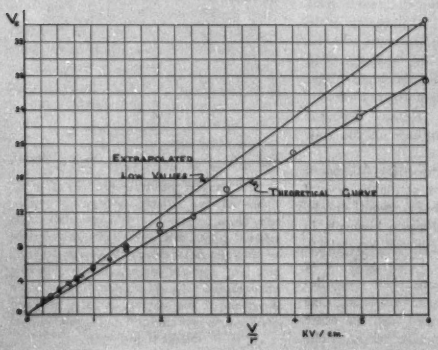
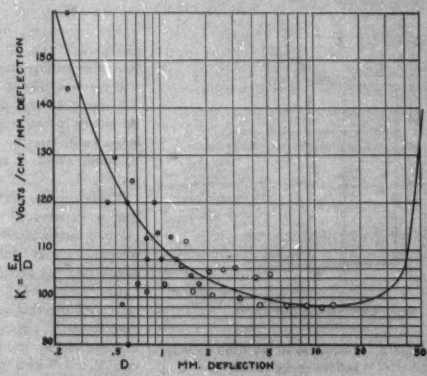


Figure 10. Corrected crystal calibration

E_m is the peak field strength in volts per centimeter computed from the applied voltage and the crystal position. D is the measured peak oscillograph deflection. The curve above 15 millimeters was obtained from Figure 7



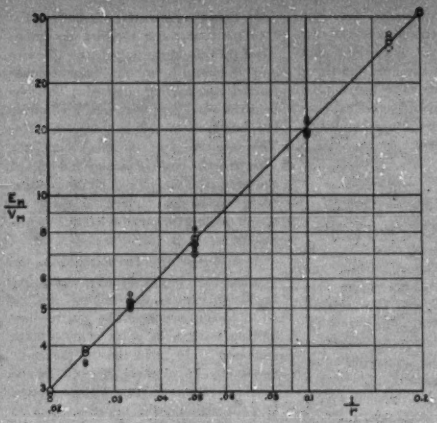


Figure 11. Corrected Position Calibration

The ordinate is volts per centimeter per kilovolt and the abscissa is reciprocal centimeters. E_m was determined from film measurements and Figure 10. The solid line is the same universal computed curve as Figure 2

diagram. The sequence of events was controlled by a synchronously-driven set of cam switches. Switches *S5* and *S6* were adjusted to close with a 0.25-second time interval between them, the time required for a single revolution of the film drum. An exposure was made by first setting the shutter in the closed, ready position. *S1* was closed and the arc light adjusted; *S2* was closed and the film speed adjusted. *S3* was closed and as soon as the cam was operating at full speed, *S4* was closed. *S5* opened the shutter and *S6* closed the shutter after one revolution of the film drum.

Figure 12. Typical oscillogram

Probes at 10 centimeters radius with 40 kilovolts effective potential applied. Note the change of slope when the corona starts at about 40 degrees after the voltage zero. The lower trace is potential, the upper trace is field strength

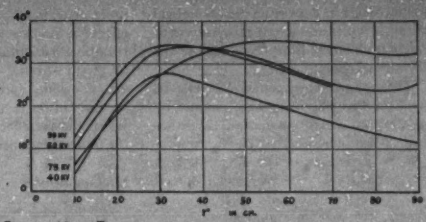


Figure 13. Time lag of the field strength zero after the applied voltage zero

Note that the peaks for successively higher voltages occur both at successively larger radii and at greater time lags

Calibrations

The crystal alone was calibrated by applying known voltages to the electrodes and measuring the deflections. These values are shown in Figure 7. The crystal assembly was calibrated by applying known high voltages to the wire-cylinder assembly, and measuring the deflections. These values are shown in Figure 8. The following notation is used:

V_c = peak volts on crystal
D = galvanometer deflection in millimeters at the film
r_1 = central wire radius
r_2 = large cylinder radius
r = radius of measurement, crystal position
V = potential difference, kilovolt effective
V_m = potential difference, kilovolt peak
E = field strength, volts per centimeter, effective
E_m = field strength, volts per centimeter, peak
C_c = capacity of crystal
C_s = capacity of probe screens
d = spacing of the probe screens, centimeters
$k = V_c/D$, crystal constant
$K = E_m/D$, field strength constant

The field strength and the crystal voltage can be computed from the applied voltage.

$$E_m = V_m/(r \ln r_2/r_1). \tag{23}$$

$$V_c = E_m dC_s/(C_c + C_s). \tag{24}$$

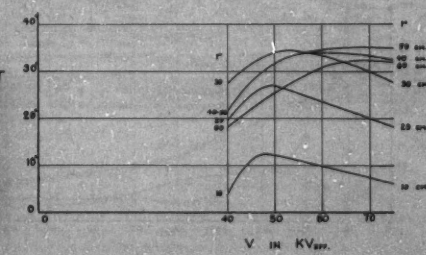
$$V_c = (V_m/r)(dC_s/(C_c + C_s) \ln r_2/r_1) \tag{25}$$

The crystal voltage is therefore a linear function of V_m/r. A curve computed theoretically from equation 25 is plotted in Figure 8 along with the experimentally measured calibration points.

The calibration points were obtained by measuring the galvanometer deflections for voltages from 7.5 to 30 kv, at radii from 5 to 60 centimeters. Figure 7 was used to get the crystal voltages from the deflections. These values were then plotted in Figure 8 versus the ratio of the effective kilovolt to the radius. Each group of points represents a single field strength at a series of radii. Deviations from a straight line indicate an error in the crystal calibration. A scattered group of points represents a capacity change with radius change, or inaccurate locating. If the straight line through the

Figure 14. Time lag of the field strength zero after the applied voltage zero

If these curves were continued, they would intersect the horizontal axis at 34.3 kilovolts. The spread between the 10 and 30 centimeter curves at 75 kilovolts indicates that the space charge occupies the region up to about 35 centimeters. The close grouping of the curves at 40 kilovolts shows that the main space charge does not extend beyond 20 centimeters



points differs in slope from the theoretical curve, it represents actual capacitances or spacings different from those used in the theoretical computation.

These same experimental data can be used for checking in another manner by plotting on co-ordinates of V_c/V versus $1/r$. These co-ordinates are the reciprocals of those used in the universal field strength curve. The reciprocals spread out the significant data a little more evenly, as is seen in Figure 9.

$$V_c/V_m = dC_s/r(C_c+C_s)\ \ln r_2/r_1 \qquad (26)$$

Each group of points represents a single radius and a span of field strengths. A scattered group means an inaccurate crystal calibration. A nonlinear curve means a calibration varying with the radius of measurement, and the lack of coincidence of the theoretical curve with the measured values is due to the same factors as before. These two figures show that a more accurate over-all crystal calibration is needed, and that there are no inconsistencies due to the position of the probes.

The most useful calibration constant for the future work is $K = E_m/D$, the ratio of the field strength to the measured deflection. This K was computed from the calibration data, using equation 23, and was plotted in Figure 10. The ordinate was exaggerated for added accuracy. The curve above 15 millimeters was obtained from Figure 7. A final check was made by using the new curve of K to replot in Figure 11 the data of Figure 9. The vertical co-ordinate is the ratio of the field strength to the voltage, instead of the ratio of the crystal voltage to the applied voltage, but the form and significance of the curve is the same. This is a corrected position calibration curve. It is a straight line, which proves that the probe position and presence does not affect the field strength and potential distributions measurably. Since the points all group together on the line, and there are no wide spreads, the percentage accuracy of the crystal and screen calibration is the same for all voltages, and is of the order of four per cent.

Measurements

Oscillograms were taken for voltages from 40 to 75 kv and at radial positions from 10 to 90 centimeters. Field strength versus distance curves, and total charge versus distance curves were plotted for 45, 90, 135, and 180 electrical degrees after the applied voltage zero for each potential. The forms of these curves were similar; the 75-kv data will be presented as representative.

A typical oscillogram is shown in Figure 12. A sharp change in slope of the field strength curve can be seen corresponding to the first burst of corona. The residual ions cause the field-strength zero to lag behind the applied voltage zero. The amount of this lag in electrical degrees is shown in Figures 13 and 14. As the voltage was increased, there was both a greater travel of the ions, and more of them, which caused the maximums of these lag curves to move up and to the right (to longer times and greater distances).

The field strength curves for 75 kv are given in Figure 15. To these were applied the technique shown in Figure 3, and the derived total charge curves are plotted in Figures 16 and 17. An analysis of these distributions shows a residual space charge, which near the central wire and outer cylinder has the same sign as the central wire had during the preceding half cycle. In the 50-centimeter region was a body of ions whose sign was opposite to that of the corona during the preceding half cycle.

At 45 degrees after the voltage zero, the total net charge near the wire was diminished by ions being pulled into the wire and discharged, and by new corona-formed ions moving outward. The ions near the outer cage were separated under the influence of the new electric field. The negative ions moved toward the wire, and the positive ions moved toward the cage, resulting in a small net positive charge at 90 centimeters. A pocket of slower moving old ions was left at 55 centimeters.

At 90 degrees after the voltage zero, the new ions have reversed the net charge out to 30 centimeters, and reduced it from 30 to 45 centimeters. The positive ions moving toward the outer cylinder have increased Q to .2 in the last 20 centimeters. The positive pocket at 55 centimeters was reduced by the motion into the region of the negatives from 75 centimeters. At 135 degrees after the voltage zero, the new ions have penetrated even further to 40 centimeters, and at 180 degrees there remained the same form of residual ion distribution as at the start of the half cycle, except with reversed signs.

Conclusion

A new experimental technique has thus been developed and tested. Its superiority lies in the ease of calibration and obtaining data. It is now practical and possible to obtain actual curves of ion densities and motions, and study their fundamental mechanisms. A method of calibration has been presented which will tell the investigator the magnitude and kind of error which the apparatus will introduce into the field. A further test of this method can be made by the application of accepted ion mobility values in the simplified quasistatic field equations,

Figure 15. Field strength versus distance for 75 kilovolts effective, 60 cycles

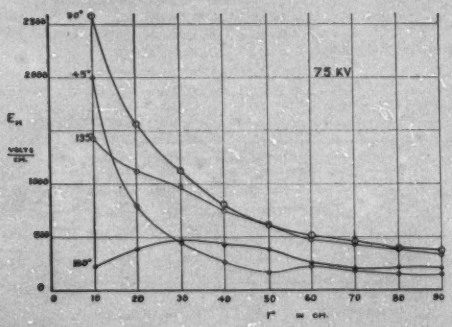
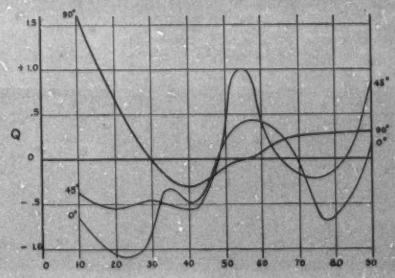


Figure 16. Total net charge versus distance for the first quarter cycle of an applied 60-cycle 75-kv potential



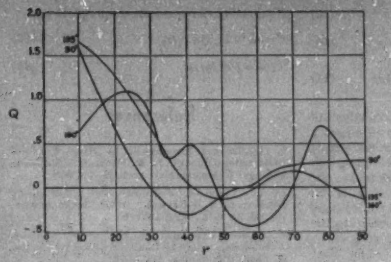


Figure 17. Total net charge versus distance for the second quarter cycle of an applied 60-cycle 75-kv potential

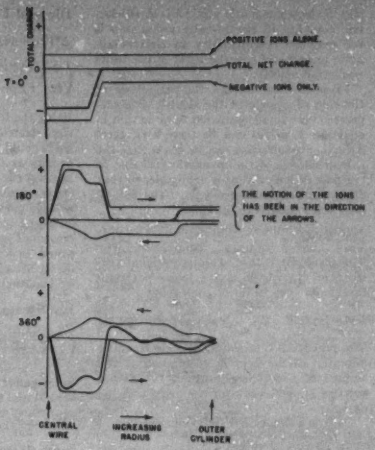


Figure 18. Artificial derivation of total net charge curves

The dotted line is the negative space charge; the light solid line is the positive space charge; the heavy solid line is the net space charge. The arrows show the directions the ions have been moving

which will yield gross results consistent with measured actuality.

The mechanism of the ion motions is similar to that represented in Figure 18. At zero time, a body of ions is assumed to be left over from the previous negative corona, and elsewhere there is a uniform distribution of positive and negative ions. During the positive half cycle, the negative ions would move toward the wire. A few low-mobility ions would be left near the outer limits of the corona-ion penetration. A large number of positive ions would be introduced due to the new corona, and these would have higher mobilities and move farther than the old negative ions coming in at large radii. At the end of the first 180 degrees, three distinct regions can be recognized. During the next half cycle, the positive ions would be pulled back, and a space charge due to negative corona fed out. Not all of the new positive ions would be collected, because some, due to aggregation, would have had their mobilities decreased. The residual net charge curve shown for 360 degrees in Figure 18 would be that expected if the physical phenomena matched this simplest of explanations. No consideration has been given to diffusion or recombination, though the magnitude of the latter determines the final values of ion densities in the middle region. Even so, this artifice results in a zero-degree curve remarkably similar to that determined experimentally. The net charge bumps in the middle region are due to pockets of ions of both signs moving back and forth through each other. The form of the distribution in this region is altered by changing either the large cylinder diameter or the applied voltage. Rough calculations of ion mobilities, by following characteristic net charge bumps in this region, yielded from three to four centimeters per second per volt per centimeter, which is not inconsistent with the precise values of other investigations.

More voluminous data would have warranted a more careful study of the derived space charge curves than has been given here. However, the main features of the observed ion motions can be explained in the preceding simple manner, and the theorem of the minimum hypothesis indicates that no further enlightenment can be obtained by the theoretical consideration of more complicated phenomena.

This work will not change methods of measuring or predicting corona loss, but the laboratory worker will be encouraged to realize that the ion densities and motions actually exist qualitatively as he has previously visualized them. Quantitatively, they can now be measured.

Appendix

Critical Corona Voltage

If a voltage is applied between a wire and a concentric cylinder, and raised beyond a certain critical value, a power loss occurs, which is accompanied by visible and audible phenomena at the surface of the wire. This phenomenon is corona, and the lowest value of voltage at which the visible glow can be seen in the dark is called the critical visual corona voltage. At this voltage, the gradient or field strength at the surface of the wire is

$$G = 31d + 9.55(d/r_1)^{1/2} \quad (1)$$

G is the field strength at the surface of the wire, necessary for corona, expressed in kilovolts per centimeter; r_1 is the radius of the wire, and d is the relative air density. The foregoing formula is equation 25a on page 64 of Peek's Dielectric Phenomena in High Voltage Engineering.[1] It was derived by considering that the breakdown gradient of air of 31 kilovolts per centimeter existed a short ionizing distance of $0.308\,(r_1)^{1/2}$ away from the wire surface.

The potential difference necessary to produce the critical field strength for a central wire in a cylinder of radius r_2 is

$$V = mGdr_1 \ln r_2/r_1 \quad (2)$$

The factor m was introduced to account for variations from the theory due to the surface conditions of the central conductor. If the potential is raised above this value, the corona increases, and generates ions around the wire. This large concentration of ions near the wire alters the field strength and potential distributions markedly.

Poisson's Equation

The problem of an electric field with distributed charge has been nicely treated mathematically. Maxwell's field equations are

$$\text{Curl } \boldsymbol{E} = -d\boldsymbol{B}/cdt \quad (3)$$

$$\text{Curl } \boldsymbol{H} = d\boldsymbol{D}/cdt + 4\pi j \quad (4)$$

$$\text{Div } \boldsymbol{D} = 4\pi\rho \quad (5)$$

$$\text{Div } \boldsymbol{B} = 0 \quad (6)$$

There are also the supplementary equations

$$\text{Div } j + \partial\rho/\partial t = 0 \quad (7)$$

$$\boldsymbol{D} = k\boldsymbol{E} \quad (8)$$

$$\boldsymbol{B} = \mu\boldsymbol{H} \quad (9)$$

$\boldsymbol{E}$ is the electric field strength in electrostatic units; $\boldsymbol{D}$ is the electric displacement vector, also in electrostatic units; $\boldsymbol{H}$ is the magnetic field strength in electromagnetic units; $\boldsymbol{B}$ is the magnetic flux density in electromagnetic units; j is the current den-

sity in electromagnetic units; k is the dielectric constant in electrostatic units; mu is the magnetic permeability in electromagnetic units; and c is the ratio of the units (radiation velocity).

In quasi-electrostatics, the magnitude of the rate of change of the electric displacement is very small compared to cj, so that in equation 4, dD/dt can be considered zero. A further assumption can be made that the rate of change of the magnetic field due to the flow of ion current is negligible, so that dB/dt can be considered zero. The first of Maxwell's equations becomes

$$\text{Curl } E = 0 \tag{10}$$

This condition is that of an irrotational vector field, for which case a scalar potential function V may be set up such that

$$E = -\text{grad } V \tag{11}$$

The potential V is simply the electrostatic potential of the field in space, and at the surface of the central wire, is the actual voltage applied between the wire and the cylinder.

Poisson's equation may now be derived.

$k = 1$, for air

$D = E$

$\text{Div } E = \text{div } D = 4\pi\rho$

$E = -\text{grad } V$

$$\text{Div grad } V = -4\pi\rho \tag{12}$$

$$\Delta^2 V = -4\pi\rho \tag{13}$$

$$\left(\frac{\partial}{\partial r} + \frac{1}{r}\right)\frac{\partial V}{\partial r} + \frac{1}{r^2}\frac{\partial^2 V}{\partial \phi^2} + \frac{\partial^2 V}{\partial z^2} = -4\pi\rho \tag{14}$$

Using the foregoing equation, an analysis of the electric potential for low-frequency fields could be made to yield the net charge density at any point. This is not practical, because the form of the equation makes the charge density sensitive to the slope of the voltage curve. Small errors in the voltage, or in the shape of the curve connecting the measured points, will cause very large errors in the charge density. The most direct attack to the cylindrical field is given by equations 5 and 8

$$\text{Div } E = 4\pi\rho. \tag{15}$$

$$\left(\frac{\partial}{\partial r} + \frac{1}{r}\right)E + \frac{1}{r}\frac{\partial E}{\partial \phi} + \frac{\partial E}{\partial z} = 4\pi\rho. \tag{16}$$

Since guard rings are used at the ends, the second and third terms are zero.

$$dE/dr + E/r = 4\pi\rho. \tag{17}$$

in the absence of chargers,

$$dE/dr = -E/r. \tag{18}$$

$$E = c'/r. \tag{19}$$

Evaluating the constant of integration,

$$c' = Er = V_1/(\ln r_2/r_1) \tag{20}$$

$$V_1/E = r \ln r_2/r_1. \tag{21}$$

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Discussion

Bradley Cozzens (Department of Water and Power, Los Angeles, Calif.): The author in presenting the new and novel method of determining voltage gradient and from it total charge in the space surrounding a wiring in corona has added further proof and possibly more accurate information upon the existence and movement of space charges surrounding such conductors. While the experiments were limited to the classical arrangement of concentric cylinders, they serve definitely to establish further the complexity and multiplicity of items which affect the formation of corona upon electrical conductors. The distortion of the normal field, as indicated in Figure 15, resulting from the migration of charges, as indicated in Figures 16 and 17, serves largely to verify the difficulties encountered in attempting to predict corona losses by theoretical means. The many variables which exist on open-wire lines, coupled with the varying nature of space charge movements, wind velocities, and normal atmospheric ionization, give further emphasis to the necessity for determining corona loss on transmission line conductors on full scale line sections.

The brush patterns which occur serve further to complicate the problem of theoretical determination, observations having indicated that corona brushes may exist as much as two or three feet apart, with no visible evidence of corona between such positions. These complications, coupled with the variable movements as indicated in this paper, have made it relatively impossible to predict without actual test, the performance of bundled conductors for transmission line operation.

As stated in the last paragraph of conclusions, the information furnished in this paper probably will not change the methods of determining transmission line corona loss or corona loss measurement. They should, however, have definite application in connection with the design of electrical precipitators and other devices utilizing corona and charge migration. A further study of this nature in connection with multiple conductors might add materially to the basic knowledge and assist in establishing proper positioning of bundled conductors in transmission line practice. The author is to be complimented on his unique methods and a complete study of an interesting laboratory problem.

Joints and Clamps for Aluminum Conductors

GORDON B. TEBO
MEMBER AIEE

Synopsis: Experience with conductor joints and clamps in a large transmission system is outlined with particular reference to aluminum-aluminum and aluminum-copper connections. Three investigations are described in which outdoor exposure tests were made on live-line tap clamps, U-bolt clamp connections between ACSR (aluminum cable steel reinforced) and copper cable, and various types of sleeve joints in ACSR. During the exposure period of five to seven years, they were subjected to a load cycle, and periodically, measurements of electrical resistance were made. Following the exposure, specimens were examined for corrosion, and subjected to heat runs and short-circuit current tests. Live-line tap clamps of the screw type were found superior to spring type. Preferred material for clamp surfaces in contact with ACSR was aluminum. Coatings of zinc or tin on bronze clamps retarded corrosion of the ACSR strands. In U-bolt clamps serious corrosion of ACSR strands occurred where they contacted clamp parts of copper, even though the copper was coated with zinc or cadmium. Aluminum separators between copper cable and ACSR maintained the lowest resistance and caused least corrosion. The use of No-ox-id lessened corrosion especially where dissimilar metals were in contact. Hydraulically-compressed sleeve joints in ACSR were superior to those pressed with hand tools or twisted sleeve joints. The resistance of twisted sleeve joints was affected more than compression joints by variations in type of filler. The application of these and other observations to field practice is discussed. The result of improved jointing techniques is illustrated by an analysis of resistance values on some 250 compression joints in ACSR measured during construction of high-voltage lines. Fields for further research are indicated.

Introduction

DIFFICULT problems confront every electrical utility in maintaining connections in electric conductors and in adding thousands of new ones each year. On an industry-wide basis, the magnitude of these problems warrants extensive research. Electrical utilities must take the initiative in solving them because their workmen make most of the connections and their plant is the proving ground for new designs and methods.

While important contributions have been made to the understanding of electric contacts between metals,[1,2] aluminum has received scant attention. Aluminum as a conductor material now predominates in several countries, and finds increasing application in others, hence the special problems of aluminum-aluminum and aluminum-copper connections require attention.

Joints in transmission lines are expected to possess high electric conductivity and mechanical strength and to retain both qualities for the entire life of the conductor. This may involve exposure to extreme variations in temperature and tension, vibration stresses, short-circuit currents, and atmospheric corrosion for a period of 50 years or more.

Predictions of such long-term performance from results of accelerated life tests are not convincing. Salt spray or fume cupboard tests may represent exposures found at the seacoast or near chemical plants, but such conditions are not common. A better basis for evaluating joints and clamps is to be found in past experience, supplemented by long-term life tests under controlled, but not highly accelerated conditions.

Experience

That it is possible to make good and durable joints in aluminum as well as copper conductors has been proved by experience extending over a 40-year period. Unfortunately, even among types which have been generally satisfactory, occasional failures occur. Less satisfactory types produce more frequent failures. Failures usually occur at times of heavy electrical loading and cause serious interruptions.

Good mechanical characteristics of conductor joints and clamps can be assured by applying well understood principles of design, selection of materials, and good workmanship. The attainment of good electrical characteristics has proved more difficult and most failures are attributed to poor electrical conductivity.

In a transmission system employing both aluminum and copper conductors, almost all troubles in electric connections are associated with aluminum or aluminum-copper contacts.

Twisted Sleeve Joints in ACSR

Although considered inferior to compression joints, twisted sleeve joints are still used in locations where hydraulic presses are difficult to manipulate. Deterioration is indicated first by increase of resistance and heating. This is followed in extreme cases by the appearance of holes in the sleeve, produced by internal arcing, and finally the conductor fails mechanically at or near one end of the sleeve. The typical external appearance of a defective twisted sleeve joint is shown in Figure 1.

Compression Sleeve Joints

Experience with compression sleeve joints has been generally satisfactory. Occasionally, however, an aluminum conductor burns off at or near a compression sleeve. Examination usually discloses improper cleaning of conductor or sleeve.

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Gordon B. Tebo is with the Hydro-Electric Power Commission of Ontario, Toronto, Canada.

The author wishes to acknowledge the guidance of the Hydro-Electric Power Commission research committee on joints which sponsored these investigations.

Figure 1. Defective twisted sleeve joint in ACSR showing holes in sleeve due to internal arcing

The resultant high resistance causes heating, arcing, annealing, and finally rupture of the conductor.

The location of this rupture is usually just at entry to the sleeve, but is sometimes a foot or more away from the sleeve. Investigation in such cases has shown high interstrand resistance in the joint with consequent uneven current distribution. The few high conductivity, but overloaded strands, become red hot in the vicinity of the joint. Maximum temperature may occur some distance from the sleeve because its mass acts as a cooling medium. An arc produced by rupture of an overheated strand or by voltage difference between strands may induce progressive failure of adjacent strands. Such failures usually occur in periods of heavy load, not under fault conditions. A typical failure is shown in Figure 2.

For the other end of the sleeve joint shown in Figure 2, the resistances between individual strands and the sleeve were measured. With three volts applied all but four strands appeared open circuited. With 110 volts applied, the resistance values shown in Figure 3 were obtained. Of the five strands showing lowest resistance, one had fused off, and the other four showed signs of extreme heating and arcing to adjacent strands.

Threaded Connections

A compression sleeve joint, shown in Figure 4, employs a current-carrying threaded connection to join the two halves of the aluminum sleeve. This type has a very bad operating record. After 20 years in service on a 110-kv line, several hundred such joints in 312,000 circular-mil ACSR were tested, and more

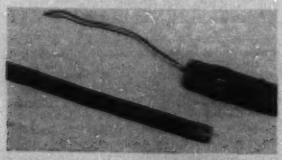


Figure 2. Failure at compression joint in 820,000-circular-mil aluminum conductor

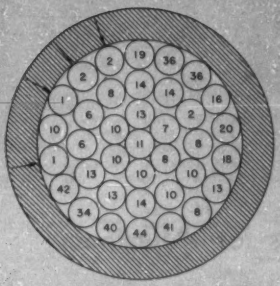


Figure 3. Resistance in milliohms between individual strands of 820,000-circular-mil aluminum conductor and sleeve measured at defective joint shown in Figure 2

Strands indicated by arrows showed signs of severe overheating

than 50 per cent had a resistance greater than ten times normal. In most cases the high resistance was in the threaded connections, not in the compression connections between sleeve and conductor. In some cases, almost all current was carried by the steel twisted sleeve. The trouble was attributed to inadequate cleaning and tightening of the threaded connection when installed.

The electrical conductivity of such joints was restored by welding across the threaded connection without lowering the conductor. However, the mechanical strength had been impaired by rusting of the steel sleeve and in one case, the joint parted during the welding operation. Fortunately the welder was supported by a line fastened to a second conductor. Most joints of this type were later replaced.

Clamped or Bolted Connections

Few actual failures have occurred at clamped or bolted connections but many have been averted by inspection and maintenance. Substitution of Everdur for steel U-bolts in clamps for large ACSR conductors proved beneficial. This was attributed to similarity of temperature-expansion characteristics of Everdur and aluminum. Where clamp connections between aluminum and copper conductors occur on important lines, they are inspected annually. When any burning of contact surfaces is found, it is on the aluminum side of the connection.

Compression Dead-end Bodies

The dead-ending of ACSR lines on copper disconnecting switches has been a

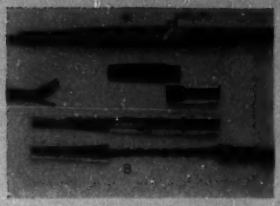


Figure 4. Threaded compression-type sleeve joint in 312,000-circular-mil ACSR

A—Welded to restore conductivity of threaded connection
B—Section of joint which failed in service

source of trouble. Recent practice has been to press bronze dead-end bodies directly on the ACSR and bolt the dead-end bodies to the copper switch. Coating the inside of the bronze sleeve with aluminum solder to minimize galvanic corrosion was, if anything, detrimental. Some failures, where the dead-end body was mounted with open end up, were thought to be due to water freezing in the sleeve and forcing the conductor outward.

Investigation of Live-Line Tap Clamps

Connection of pole-type transformers to primary conductors by means of live-line tap clamps is a growing practice. Anticipating some 100,000 installations, it was desired to evaluate two available designs

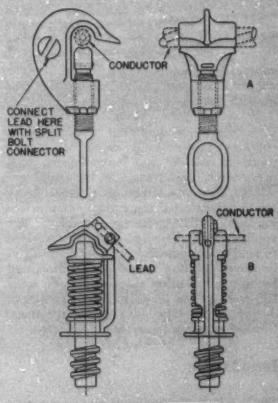


Figure 5. Live-line tap clamps used in five-year exposure test

A—Screw-type
B—Spring-type

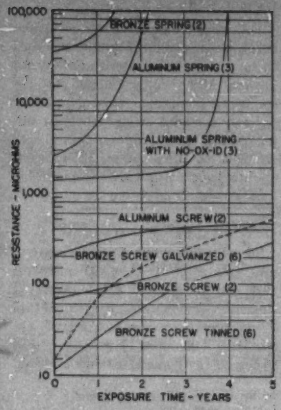


Figure 6. Resistance of live-line tap clamps on number 4 ACSR during five-year exposure test

Numerals in brackets indicate number of specimens included in average plotted

of clamp, and to determine what clamp materials would be suitable for use on aluminum conductors. It was decided that only a long-time exposure test would satisfactorily answer these questions.

Procedure

Short spans of number 4 ACSR were erected in Toronto, Canada, near the shore of Lake Ontario. This was considered a normal urban exposure, combining high humidity and industrial smoke but free of salt spray or concentrated chemical fumes. On these spans were erected 37 clamps of the two types shown in Figure 5.

Clamp materials included aluminum,

Figure 7. Corrosion of ACSR strands at bronze live-line tap clamp due to five-year exposure test

Deep corrosion cracking is evident at edge of clamping area

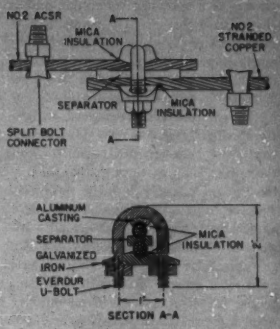


Figure 8. Specimen U-bolt clamp connection between ACSR and copper subjected to five-year exposure test

bronze, and bronze coated with zinc or tin. The contact surfaces of clamp and conductor were cleaned with emery, and immediately assembled. The screw-type clamps were uniformly tightened to produce a load of about 1,500 pounds on the contact surfaces. The spring type was designed to exert a force of 40 pounds. An alternating current of 0.5 ampere was carried continuously by each clamp connection.

Summary—Live-line Tap Clamps

On the basis of resistance values measured annually, shown in Figure 6, and visual examination at the end of the five-

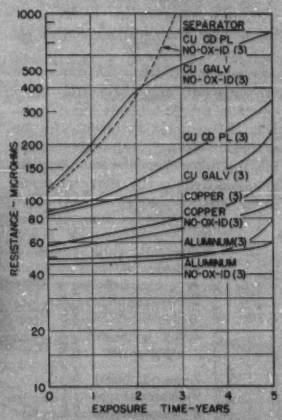


Figure 9. Resistance between ACSR and separators of various materials during five-year exposure test on U-bolt clamp connections between ACSR and copper cable

Figure 10. Condition of ACSR after five-year exposure test on U-bolt clamp connections classified as:

A—Satisfactory—minor pitting
B—Unsatisfactory—deep pitting
C—Serious—strands burned by arcing

year exposure, the following observations were made:

All spring-type clamps developed resistances exceeding 1,000 microhms which is considered unsatisfactory. Treatment with No-ox-id delayed but did not prevent increase of resistance.

Screw-type clamps of any material tested maintained resistance values less than 1,000 microhms. This is considered satisfactory since at a normal current of 10 amperes, the maximum loss would be 0.1 watt.

Uncoated bronze clamps caused serious corrosion of aluminum strands as illustrated in Figure 7.

Coating of tin or zinc on bronze clamps did not materially affect resistance values, but did lessen corrosion of the ACSR. However, the zinc (hot dip galvanizing) had corroded exposing bronze in the contact area.

The threads of the all-aluminum screw

Table I

Condition of ACSR Strands	Type of Separator in Contact with ACSR
Satisfactory	Aluminum Aluminum, No-ox-id Copper, No-ox-id Copper, galvanized, No-ox-id
Unsatisfactory	Copper Copper, galvanized Copper, cadmium plated
Serious	Copper, zinc plated Copper, cadmium plated, No-ox-id

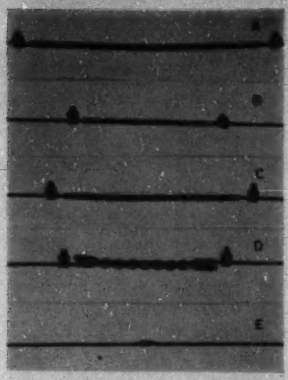


Figure 11. Sleeve joints in number 2 ACSR subjected to seven-year exposure test

A—Long sleeve, hydraulic press, hexagon die
B—Short sleeve, hydraulic press, round die
C—Pressed with hand tool
D—Twisted sleeve
E—Weld to insure uniform current distribution between strands

clamps were undesirably tight after exposure. The threads of the bronze screw clamps were satisfactory.

Investigation of U-Bolt Clamps Connecting ACSR to Copper Cable

In a distribution system, connections between aluminum and copper occur in such forms as bolted connections, compression joints, and clamps. A simple form of specimen was sought, variations of which could be subjected to the same outdoor exposure as described for live-line clamps. It was thought that basic data obtained from such specimens would be applicable to other types and sizes.

Procedure

A single U-bolt clamp, joining number 2 ACSR and number 2 copper cable was chosen as a specimen connection and is shown in Figure 8. The clamping surfaces were prepared by cleaning with dry emery paper, followed in some cases by coating with No-ox-id. The 3/8 inch Everdur U-bolts having 16 threads per inch were uniformly tightened with a torque of 100 inch pounds. Mica inserts were used between each conductor and the outer clamp castings so that all current would pass through the separator. The separator materials tested were aluminum, copper, copper plated with zinc or cadmium, and copper hot dip galvanized. Forty-eight such specimens in 2 groups of 24 each were connected in series, placed in an outdoor span, and carried an alternating current of 150 amperes, five minutes on and eight minutes off, for a five-year period.

Resistance

During the exposure period, resistance between each conductor and the separator was measured at least annually. The resistances from ACSR to the various separators for one group are plotted in Figure 9. The resistances between the separators and the copper conductors were not plotted, but remained below 70 microhms with the exception of five of the 48 specimens. In each of these five cases the resistance on the aluminum side first exceeded 1,000 microhms which probably caused heating and oxidation of the copper side. It is interesting to compare the resistance of aluminum separator to copper cable with that of copper separator to aluminum cable. After exposure, the resistance values for six specimens between aluminum separator and copper cable were 44, 31, 49, 42, 33 and 35 averaging 38 microhms. The corresponding resistance values between copper separator and ACSR were 285, 170, 82, 175, 140 and 132 averaging 164 microhms. This result leads to the observation that when aluminum and copper are to be joined electrically, aluminum should be the massive member.

Damage to ACSR

Following the exposure test, one specimen from each group of three was cleaned by an appropriate American Society for Testing Materials procedure and inspected for corrosion. The only member seriously corroded in any specimen was the ACSR, hence the specimens were classified into three groups according to condition of the ACSR as shown in Table I and illustrated in Figure 10.

Temperature Rise

Six specimens which covered a range of resistance from 90 to 2,000 microhms were selected for heat run tests at 150 amperes. From this test it appeared that clamp resistance must not exceed 300 microhms if a limiting temperature rise of 50 degrees centigrade is to be maintained.

Short Circuit Current Test

After dismantling one specimen from each set of three for examination, the remaining specimens were subjected to short circuit current tests. All clamps carried 1,000 amperes for two seconds and then 2,000 amperes for two seconds without visible evidence of damage and without any major change in resistance value. Currents were then increased until most specimens showed burned-off strands in the number 2 ACSR. The results of these tests are summarized:

All specimens with resistance below 1,000 microhms withstood at least 2,000 amperes for two seconds.

All specimens with resistance below 100 microhms withstood currents of 4,000 amperes for two seconds.

The mica inserts used in assembling these specimens caused three or four of the six aluminum strands to carry the total current. In view of this restriction, and the rather long duration of the test current (two seconds), it appears that short circuit currents of normal duration and several thousand amperes could be carried by specimens with resistance up to 1,000 microhms.

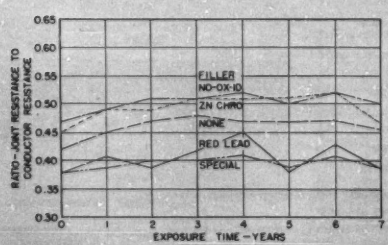
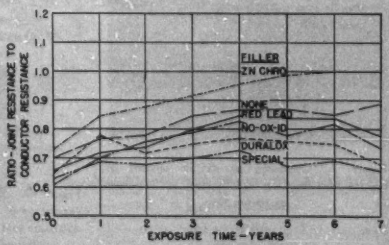
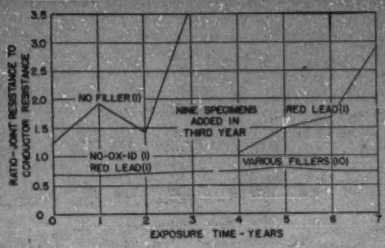


Figure 12 (left). Resistance of type A compression sleeve joints in number 2 ACSR during seven-year exposure test

Figure 13 (right). Resistance of type B compression sleeve joints in number 2 ACSR during seven-year exposure test





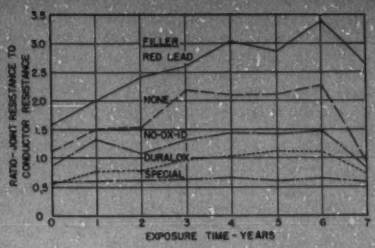


Figure 14 (left). Resistance of type C sleeve joints in number 2 ACSR compressed with hand tool and subjected to seven-year exposure test

Figure 15 (right). Resistance of type D twisted sleeve joints in number 2 ACSR during seven-year exposure test

Summary—U-bolt Clamps

When excessive resistance developed in U-bolt clamp connections between ACSR and copper, it appeared first between the separator and the aluminum conductor.

Of the separators tested between ACSR and copper cable, aluminum was best in maintaining low electrical resistance and was satisfactory from a corrosion standpoint.

Coatings of zinc or cadmium on copper separators were detrimental to the maintenance of low electrical resistance and did not prevent corrosion and burning of the aluminum strands.

Treatment with No-ox-id had no beneficial effect on resistance values. However, it reduced corrosion of the ACSR strands throughout the five-year period, and appeared capable of giving protection for many more years except where burned away in high resistance connections.

U-bolt clamps for connecting number 2 ACSR to number 2 copper cable which maintain resistances of less than 300 microhms are considered satisfactory with respect to energy losses, temperature rise, and short circuit currents.

Investigation of Sleeve Joints in Number 2 ACSR

Procedure

In order to compare the performance of various types of sleeve joints in ACSR and the various filler materials available, four 150-foot spans of number 2 ACSR were erected outdoors. In these were installed 87 sleeve-type joints, tension was adjusted to 150 pounds and current of 150 amperes, five minutes on and eight minutes off, was maintained through the joints.

No attempt was made to clean the inside of the sleeves. The conductor was cleaned by rubbing the outer surface with dry emery cloth. In order to maintain uniform current distribution, all six-conductor strands were welded together midway between sleeve joints. The four types of sleeve joint tested and a sample weld are shown in Figure 11. With these were used various types of filler; No-ox-id grade A special, supplied by Dearborn Chemical Company, zinc-chromate in linseed oil, red lead in linseed oil, Duralox which is a mixture of No-ox-id and dur-aluminum filings, and a special joint compound also available from Dearborn Chemical Company.

Resistance

Resistance readings were made periodically and the results of a seven-year exposure are shown on Figures 12, 13, 14, and 15. The resistance readings are reduced to ratio of joint resistance to that of an equal length of conductor. Joints are usually considered satisfactory for ratios of less than one.

The sudden decrease in resistance of the twisted sleeve joints shown in Figure 15 at the sixth year was due to the breaking of a pole supporting the spans. The readings for the seventh year were taken after the fallen spans had been re-erected.

Summary—Sleeve Joints in ACSR

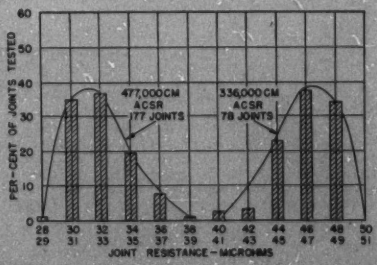
All sleeve joints hydraulically compressed (types A and B) maintained satisfactory resistance values through the seven-year period.

Sleeve joints compressed with hand tools (type C) gave erratic results.

Of the five groups of twisted sleeve joints (type D) only one had a resistance ratio less than one at the end of six years.

Figure 16. Distribution of joint resistances measured during line construction. Consistently low resistance values are attributed to design of extruded tubing sleeve, adequate cleaning procedure and proper compression

Sleeves for both 477,000-circular-mil ASCR and 336,000-circular-mil ASCR are approximately 25 inches long. Resistances of equivalent lengths of each conductor are 77 and 110 microhms respectively



The effect of the filler material on resistance value is more pronounced in the case of twisted sleeve joints than with compression joints. The filler marked "special" gave the lowest and most consistent resistance values in both twisted and compression joints.

Applications

Before the value of accumulated experience and data can be realized, they must be applied to improve existing practices. Some changes suggested by these investigations are shown in the following.

Cleaning Procedure for ACSR

The most important single requirement for good joints in aluminum conductors is that both sleeve and conductor surfaces be free of dirt and oxide films at the time the connection is made. To ensure cleanliness, sleeves for compression or twisted joints are now cleaned by dipping in caustic soda (Gillets Lye Solution), rinsed in water and coated with No-ox-id. The ends are plugged with cotton batting to keep the bore clean during shipment. This preliminary cleaning is followed in the field by wire brushing immediately before assembly.

Aluminum conductors, if old or blackened, are cleaned in the field by dipping in caustic soda, rinsed, and then subjected to abrasive cleaning. For the abrasive cleaning of single layer cables, emery cloth and Duralox is applied without

unlaying the strands. In multilayer cables, the outer layer or layers are unlayed, the inner layer cleaned with emery cloth and Duralox and successive layers similarly cleaned after re-laying. The cleaning procedures briefly outlined here have been written as a detailed operating instruction for field use.

Use of Filler Material in Joints

No filler material has been found, even among those containing metal filings or abrasives, which is a substitute for proper cleaning. The filler does tend to seal the interior of the joint from the atmosphere. Present practice is to use Duralox as a filler for all compression or twisted sleeve joints in ACSR. As a result of recent investigations, consideration will be given to the "special" filler. The use of red lead, zinc chromate, and similar paint fillers has been discontinued.

Separators for Copper-Aluminum U-Bolt Type Clamps

Recent practice for U-bolt clamps connecting small copper to aluminum conductors has been to use a separator of cadmium-plated copper. During the five-year exposure test, this type developed high resistance and caused deep pitting of the ACSR. Consideration is now being given to the use of an aluminum separator.

Live-Line Tap Clamps

Spring-type clamps have been found unsatisfactory due to the small contact pressure, and their use has been discontinued. Bronze screw clamps now widely used on ACSR appear harmful to the conductor. Coatings of zinc or tin on bronze clamps retard corrosion but are not a lasting preventive. The preferred clamp for use on ACSR is aluminum but because the thread of all-aluminum clamps tends to seize, it may be preferable to face the contact surfaces of a bronze clamp with a substantial thickness of aluminum.

Dead-end Bodies for ACSR

Present indications are that for connecting ACSR to copper switches, dead end bodies should be aluminum thus making the aluminum-copper junction at the bolted connection.

Field Testing of Joints

The final evaluation of the techniques now in use for making joints will be available only after many years' service. Nevertheless, some means of predicting future performance can be found from past experience. It has been observed for instance that for a given type, joints with the lowest initial resistance are most likely to remain low in resistance.

An instrument was developed for determination of resistance of joints being erected in new lines. It consists of an ammeter-millivoltmeter combination arranged for separate current and potential connections to the conductor, and supplied by a 6-volt storage battery. The results for some 250 new joints in 336,000- and 477,000-circular-mil ACSR are shown in Figure 16. These were compression sleeve type using the new extruded tubing sleeves.

This analysis shows that the resistance of the poorest joint in 477,000-circular-mil ACSR is lower than that of the best joint in 336,000-circular-mil ACSR. Also the maximum resistance value for each size of joint is less than half that of an equal length of conductor. Such results as these, in addition guarding against defective workmanship, are some assurance that the joints will remain good joints for the life of the line.

Future Program

Much remains to be done to consolidate the present knowledge of electric connections into engineering practice and to meet new problems as they arise. Profitable fields for research would include:

An exposure test for a 10 to 20 year period on simple specimen connections between various combinations of conductor materials. Observations would include periodic determination of resistance and corrosion. Variables to be investigated would be material, cleaning technique, unit pressure, shape, and coating. This should provide quantitative data for design of connections and selection of materials.

An investigation of current distribution between strands of large aluminum conductors at joints or clamp connections.

A search for a cleaning fluid or paste to simplify the present cleaning procedures for aluminum.

A search for a simple soldering or welding technique to join aluminum conductors.

Development of improved compression tools combining the lightness of hand tools with high pressures of hydraulic tools

References

1. Electrical Contacts (book), G. Windred. D. Van Nostrand Company, New York, N. Y., 1940.

2. Electric Contacts, Ragnar Holm. Hugo Gebers Forlag, Stockholm, Sweden, 1946.

Discussion

Figure 1

Frank Noakes and Shang-Jen Tsou (The University of British Columbia, Vancouver, Canada): We are very much interested in Tebo's paper as it ties in with some of the investigational work now being conducted at the University of British Columbia on techniques of making joints in aluminum cable steel reinforced (ACSR) conductors.

In the conventional ACSR compression joint the path of the electric current in the outer strands passes through two mechanical contacts, while for the inner strands the number of contacts is considerably more. We recognize that the present techniques of making compression joints is considered by many to be satisfactory. However, it is obvious that a continuous metal joint would be superior. Recent advances in welding make the construction of such a joint possible. Figure 1 of the discussion indicates how each strand of a 397,500 circular mil conductor may be brought out between two aluminum sleeves in preparation for "Heliarc" welding. In the finished joint the weld bonds the inner and outer sleeves and the individual conductors. The ends of the joint are compressed to prevent ingress of moisture which may be injurious to the conventional steel compression sleeve which connects the steel cores together. It was found, following a suggestion in the author's paper, that the individual strand resistance as measured between the central part of the joint and strands was uniform and equal to that of an equivalent length of strand. This shows that each strand was satisfactorily connected at the weld.

Over-all resistance, heat and mechanical tests indicate that the joint should be satisfactory in the field.

A Universal Telemetering System

L. DALE HARRIS
ASSOCIATE AIEE

IT IS BELIEVED that there is a need of a general telemetering method that can be applied to any one of a wide variety of telemetering problems. There is a need for some basic circuit or device that could be adapted readily to the metering of flow in a remote gas main, temperature in the cylinder wall of a remote aircraft, the current in a remote alternator, or the remote indication of any one of numerous measurable quantities of engineering interest. If the engineer merely had to adapt a basic telemetering device through minor modifications, the design of a device or circuit for a new telemetering problem might be simplified considerably. In this paper is found the discussion of such a basic telemetering circuit.

The particular telemetering system under discussion employs frequency as a translation medium; that is, the quantity to be metered at a distance is translated to an equivalent frequency at the sending end for the purpose of transmission. A method is described herein for converting various measurable quantities to equivalent frequencies for transmission over pilot wires, carrier current or conventional radio links. At the receiving end of the system the frequency is converted back to direct current or voltage in proportion to the original quantity to be metered remotely. This translation for transmission is needed to avoid the effect of changing transmission characteristics on the indication at the receiving end of the system. These various functions are performed by vacuum tube circuits which operate so that the accuracy of the indicator at the receiving end is essentially independent of drifting tube characteristics. A minimum number of moving parts are employed. In the telemetering of many of the common quantities, no moving parts are in the system except the voltmeter or ammeter used as the indicator at the receiving end. For example, an a-c teleammeter employs only tube circuits using simple conventional components at both the sending end and receiving end plus a simple d-c milliammeter as an indicator at the receiving end.

The over-all accuracy of this telemetering system is good. A particular laboratory model of an a-c teleammeter had a maximum error of 0.5 per cent.

General Principle

For this system of telemetering to find application, either the quantity to be telemetered must be a direct voltage or there must be a way of converting the quantity to an equivalent direct voltage. Fortunately, many measurable quantities can be converted to their respective equivalent direct voltages. Some examples are alternating and direct current, alternating voltage, resistance, power, flow, position, temperature, and pressure.

In general the telemetering system will include a d-c televoltmeter, a complete telemetering system, as illustrated in Figure 1, plus a converter at the sending end for converting the quantity to be metered remotely to a direct voltage. The d-c televoltmeter is basic to all applications of this universal system. Each new application merely requires that the quantity to be metered remotely must be converted to its equivalent direct voltage, then the addition of the basic d-c televoltmeter yields a complete telemetering system.

Figure 1 is a block diagram of this basic telemetering system, a d-c televoltmeter. Both of the converters shown in Figure 1 are tube circuits. The transmission circuit carries a low-frequency signal that may be amplified, detected, become a part of a modulated high-frequency signal, or otherwise manipulated in any of the conventional communication operations. Within wide limits, the output of the frequency-to-direct voltage converter is practically independent of the magnitude of the received signal. For this reason there can be no important error introduced into the d-c indicator at the receiver end by virtue of the ordinary changes in the transmission circuit and its associate apparatus.

The transmitter unit of telemetering systems other than the d-c televoltmeter must include an additional function as was mentioned previously. As an example, Figure 2 illustrates an a-c teleammeter. The added device, alternating current to direct voltage converter, is simply a rectifier and filter circuit. Block diagrams of other telemetering systems, operating on the principles described here will differ from that of Figure 2 only in the first block. The two functions illustrated in Figure 1 are common to all of the telemetering systems described in this paper.

The Receiver

Previous discussion has indicated that the receiver in this system must convert the frequency of the transmitted signal into its equivalent direct voltage to operate an indicating instrument to show the magnitude of the quantity that is in the process of being telemetered. Two separate converter units, each of which separately performs the foregoing function, are used in this telemetering system. One is used as part of the transmitter, and another is the complete receiver of the system. The lower half of Figure 3 showing the three vacuum tubes and the associated capacitors and resistors is a schematic diagram of the converter that is used in the transmitter. Since the schematic for the receiver unit is identical to that of this part of the transmitter, a separate figure for the schematic of the receiver does not appear in this paper.

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L. DALE HARRIS is with the University of Utah, Salt Lake City, Utah.

At both the receiver and the transmitter, these circuits act independently as commutating circuits each to produce a direct voltage across R_3 that is essentially proportional to the frequency of the input signal appearing at the common terminal of the capacitors marked C. At any particular instant, the vacuum tube commutating circuit at the transmitter is receiving the same a-c signal as is transmitted to the receiver unit of the system.

In the receiver unit the voltage across R_3 or the current in R_3 is a measure of the quantity that is metered remotely; in the transmitter unit the voltage across R_4 is subtracted from the d-c input for a purpose to be discussed later.

These three triodes with the associated circuit elements operate such that capacitor C_1 is charged through tube number 2 to a constant direct voltage, a voltage regulated by one of the VR tubes, during each positive half of the cycle of the a-c input wave that appears at the two capacitors marked C. During each negative half of the cycle of the input wave, C_1 is discharged through tube number 3 into C_2. Capacitor C_2 discharges at a comparatively steady rate through R_3 or the indicating instrument that might be substituted in the place of R_3. During the positive half of the input cycle, tube number 2 is conductive and tube number 3 is blocked, and during the negative half of the cycle the reverse is true. Tube number 1 is used to give a 180 degree phase angle shift in the alternating voltage supplied to the grid of tube number 3. The capacitance of C_2 is very much larger than that of C_1 so that voltage across C_2 may be kept very small compared to the maximum voltage across C_1. In this manner C_1 delivers an essentially fixed charge to C_2 during the process of each cycle of the input a-c wave. Therefore, the total charge delivered to C_2 during a given period of time is practically proportional to the frequency of the input a-c wave. The time constant of the R_3—C_2 circuit is made large in comparison to the period of the lowest frequency of the input signal such that the pulses of charge delivered to C_2 are filtered to give a rather constant direct current in R_3 that is proportional to the input frequency. The time constants of the circuits for charging and discharging C_1 are made very small compared to the period of the highest frequency that might appear in the input signal. In this manner, C_1 is allowed to charge completely and then discharge completely during each cycle of the input signal.

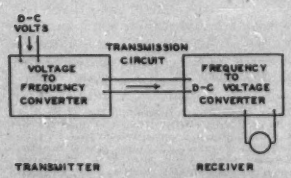

Figure 1. Basic features of the universal telemetering system—a d-c televoltmeter

The Transmitter

The transmitter (Figure 3) of the d-c televoltmeter includes an electronic oscillator of the conventional saw-tooth type having its frequency controlled accurately by a small direct voltage applied to the pentode of this oscillator through a d-c amplifier stage. The details of this oscillator are discussed later. The direct voltage that controls the frequency of the pentode E_o, is the difference between two voltages: the d-c input voltage, E_{in}, and the voltage across R_4, E_4, and it will be shown presently that these two voltages are nearly equal. For purposes of this particular discussion, it is sufficiently accurate to assume that the frequency, f, of the oscillator, the transmitted frequency, is proportional to E_o, and that the commutating circuit operates such that E_4 is proportional to frequency. These conditions are written in an equation for steady-state conditions:

$$E_o = E_{in} - E_4 \tag{1}$$

$$= K_o E_o \tag{2}$$

$$E_4 = K_c f \tag{3}$$

Eliminating f and E_o leads to

$$E_4 = \frac{K_o K_c}{K_o K_c + 1} E_{in} \tag{4}$$

It is also to be noted that:

$$K_o K_c = \frac{E_4}{E_o} \tag{5}$$

From equation 4 it is seen that by making $K_0 K_c \gg 1$, E_4 approaches E_{in} in magnitude; thus we say that E_4 is made to track E_{in}. Now if the commutating circuit in the receiver is identical to the commutating circuit in the transmitter, E_4 at the receiver will also track E_{in} at the transmitter, or an instrument indication at the receiver tracks the voltage E_{in} at the transmitter.

In order to reduce the tracking error between E_4 at the receiver and E_{in} at the transmitter, the coefficient $K_0 K_c$ is made larger by inserting a d-c amplifier ahead of the grid of the pentode oscillator. However, there must always be a slight difference between E_4 at the transmitter and E_{in}. Therefore, if both transmitter and receiver commutating circuits had identical conversion characteristics, there may be a significant error between transmitter d-c input voltage and E_4 at the receiver. To reduce this error below significant magnitudes, it is only necessary to make a slight compensating difference in the conversion characteristics of the two converters.

Figure 4 shows a schematic of the saw-tooth oscillator circuit used as the source of the transmitted a-c signal. The direct voltage, E_o that controls the frequency of the oscillator is applied to the grid of a pentode through a d-c amplifier. The control grid voltage of the pentode fixes the anode current of the pentode. This anode current charges capacitor C at a steady rate until the grid-to-cathode voltage of the gas triode allows this tube to conduct and discharge the capacitor in a relatively short time. After C has been discharged, the arc is extinguished in the gas triode and the cycle then repeats itself resulting in a saw-tooth like voltage wave appearing across C. The rate at which C charges up and therefore the period or frequency of this wave or signal are fixed by the magnitude of the anode current of the pentode. Since this current is approximately proportional to the control voltage, E_o, the frequency of the oscillator is controlled by E_o.

Performance of a Telemetering System

Depending on the desired speed of response, the maximum frequency transmitted by the telemetering set need only be from 10 to 100 cycles per second. This is a significant fact when many telemetering, control, and communication signals must be transmitted over one channel, since the frequency spectrum consumed by each telemetering signal is propor-

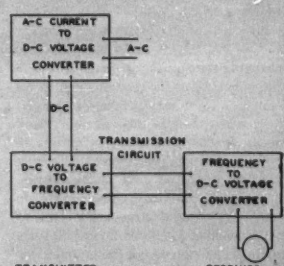

Figure 2. Basic features of an a-c teleammeter

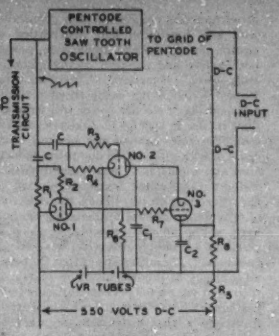

Figure 3. Transmitter—content of left-hand block of Figure 1

tional to the maximum frequency of the telemetering signal. Audio signals of moderate frequency can be used as subcarriers of several of these low-frequency telemetering signals so that many audio carriers can be impressed on one radio frequency carrier.

Within wide limits, the performance of the converters is made to be independent of the wave shape of the transmitted signal. To realize this result, it was necessary only to make the time constant for charging and discharging C_1 very small compared to the period of the telemetering signal of highest frequency. Since wave shape is of little significance, one may filter harmonics from the generated saw-tooth wave, clip the top, or perform other wave shaping operations with no significant effect upon the accuracy of the system.

In order that the telemetering error becomes very small, the conversion characteristic of the frequency to direct voltage converter in the transmitter unit is made slightly different from the conversion characteristic of the receiver. Figure 5 shows the result of this compensating change. The need for such compensation might be eliminated by introducing additional amplification ahead of the grid of the pentode in the saw-tooth oscillator.

The vacuum tube circuits in this system are of such a design that a change in any tube characteristic of ordinary magnitude will have a negligible effect on the accuracy of the system. This situation is achieved through the following two conditions:

1. Time constants of capacitor charging and discharging circuits are made very short.

2. The amplifier supplying the control voltage to the pentode in the sweep oscillator and the sweep oscillator itself are elements in a closed feed-back circuit so that any change in the operating characteristics of any element of this closed circuit is most wholly corrected in the action of a closed feed-back circuit.

In contrast to the vacuum tubes, the voltage regulator tubes are in such a position in the system, that any change in tube drop may produce an appreciable error in the system. However, these tubes generally speaking, are relatively stable in respect to time and ambient temperature; and furthermore, some selected tubes from an ordinary stock have characteristics of excellent stability.

For most applications, it is desirable that the volt-ampere consumption or burden of metering devices be small. In this telemetering system, the burden is directly related to the impedance of the input circuit to the initial converter unit that delivers the necessary d-c input voltage to the transmitter. For many applications this initial converter unit is another vacuum tube circuit, making it possible for the burden to be very small.

The over-all conversion characteristics of this system are essentially linear so that the system is applicable to telemetering totalization processes.

Telemetering of Power

Although this telemetering method is directly applicable to the remote metering of many different quantities; special problems arise relative to the telemetering of power. As far as the writer knows, there is no extremely simple circuit or device available that will accurately convert power to an equivalent direct voltage. Laboratory work indicates that electronic wattmeter circuits will not yield accuracies within one per cent where the voltage and power factor may change over rather wide ranges. Other devices that incorporate the basic wattmeter or watthour meter movements might be used but at the loss of simplicity. Thermal converters have possibilities also.

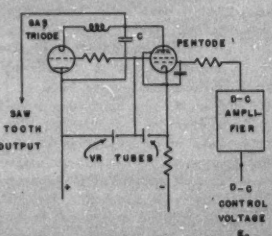

Figure 4. Schematic diagram of the saw-tooth oscillator

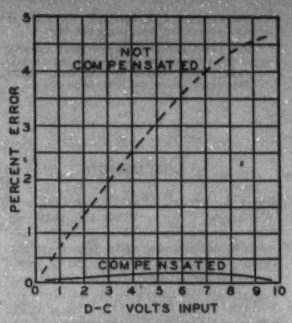

Figure 5. Effect of compensating adjustments to bring the telemetering error within desired limits

Conclusion

This simple telemetering system is applicable wherever the quantity to be metered remotely can be converted to an equivalent alternating or direct voltage. There are no moving parts in the basic system except the indicating instrument at the receiving end. The principle of operation is simple, incorporating only elementary tube circuits. Drift in tube circuit characteristics are ineffective in producing error. Volt-ampere burdens drawn at the input terminals at the transmitter ordinarily would be very small. The system is applicable to telemetering problems where totalizing is desirable or necessary.

Each telemetering signal requires a narrow band width. The maximum signal frequency that must be transmitted need not be more than 20 to 100 cycles per second depending on what speed of response is required. By using 100 cycles per second as the maximum signal frequency, the speed of response becomes of the same order as the speed of response of the ordinary instrument movement.

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Discussion

Perry A. Borden (The Bristol Company, Waterbury, Conn.): While this paper describes an ingenious and presumably novel method of obtaining a frequency proportional to unidirectional potential, it would seem that the author has overlooked at least one serious limitation to the universal use which he claims for it. This is found in the necessity for translating the measured magnitude into a suitable voltage before the measurement enters the telemetering system proper. If the potential were of the order of 0.1 volt or less, the limitation would not be so severe. Ammeter shunts, thermocouples, and thermal converters can be utilized to obtain these relatively low potentials and thereby reproduce with a good degree of precision the respective quantities under measurement. The only information which the author has given with respect to the order of magnitude called for in his d-c controlling potential is that which may be implied from the curve in Figure 5, where the compensation is shown through a range of 0–10 volts. Not only is it impracticable to obtain such potentials from the devices mentioned before; but if these voltages were available, they could in many instances be directly utilized for the purposes of telemetering without the need of interposing the a-c link which forms the basis of this paper. The obtaining of such voltages as representative of the small potentials mentioned would almost invariably require the use of some form of servomotor, which would add to the complexity of the system, and which, if used, would in itself frequently provide a complete solution to the telemetering problem.

Where the transmitted magnitude is inherently associated with considerable mechanical force, as in the case of liquid-level gauges or position indicators, it is of course possible to utilize an adjustable slide-wire, which, while providing a convenient source of available voltage, demands a regulated potential supply. There would seem no object in employing the system under consideration in these instances where the measured quantity is basically represented by the deflection of a moving shaft or spindle.

The author's statements with respect to low volt-ampere consumption are somewhat confusing. The actual telemetering system which he has described would presumably obtain its power supply from a commercial service, where the restrictions or power consumption are not severe. As to the burden impressed upon any measuring circuit associated with the transmitter, it is not seen that (with the possible exception of measurement of d-c potential) the volt-ampere consumption is any less than that required by the primary measuring elements of the more common systems of telemetering.

It is noted that the receiving instrument converts the frequency of the transmitted signal into an equivalent voltage to operate a d-c indicating instrument. If, as is very common practice, a graphic record of the telemetered quantity be required, it will be necessary to utilize either a powerful direct-marking millivoltmeter, or, better, a self-balancing recorder of the potentiometer type. Either of these instruments superimposed upon the unit which converts the frequency into its equivalent d-c potential, will offset any simplification which might characterize the basic system, and eliminate such advantage as might otherwise be claimed over those systems in which the signal as received directly actuates the exhibiting instrument either in an indicating or a recording form.

H. C. Thomas (General Electric Company, West Lynn, Mass.): The telemetering system described by Harris has many desirable characteristics, foremost of which are fast time of response with continuous presentation. The use of the frequency of an electric signal as the translating means is particularly advantageous when the telemetering is accomplished over a carrier-current channel. An analytical evaluation of the equipment indicates that it should be relatively free from maintenance difficulties.

At the present time, a very important use of telemetering equipment is in connection with the generation and distribution of electric power. In this application, power, both active and reactive, is the quantity most often telemetered. The fact that Harris' system is not easily adopted to the telemetering of electric power will seriously limit its use in this field. Likewise, its use over pilot-wire channels may be limited because of high attenuation due to line inductance and capacitance, which are important considerations where frequency is used as the translating means. Thus, the objective of providing a general telemetering method that can be applied universally, which is certainly a desirable objective, has not yet been met. The author states that the overall accuracy of this telemetering system is good, and cites a 0.5 per cent accuracy for a particular laboratory model. It is important to recognize that instrument accuracy is closely associated with the conditions under which the instrument is used, and that the accuracy attainable in a laboratory more often than not is unattainable in any practical application.

The need to compensate for conditions in the transmitter by adjustments in the receiver may prove to be a serious objection with regard to flexibility of application and simplicity of operation. Ideally, the transmitter and receiver of a telemetering system should be independent of each other with regard to adjustments and calibration.

The output wave shape of the transmitter may lead to serious interference problems because of the large percentage of harmonics which it contains. This is especially true when the telemetering is accomplished over the type of carrier-current channel which provides several telemeter channels for each carrier frequency, using audio tones and double modulation. Also, the number of telemeter channels which can be provided using a single carrier frequency is less than could be provided if the output signal contained no harmonics.

To summarize, it appears that this telemetering system is very well suited for the telemetering of d-c quantities over some types of carrier-current channels, in applications where a high degree of accuracy and stability is not required. Its fast, continuous response makes it adaptable to control initiation, and its simplicity should result in minimum maintenance requirements.

Electrification of an Ore Haulage System

R. J. CORFIELD
MEMBER AIEE

TO HAUL, feasibly and economically, 30,000,000 tons of ore annually a distance of 14 miles posed a specific problem to the Kennecott Copper Corporation at its Utah properties. This problem has been solved by building an electric industrial trackage line between the open-cut copper mine at Bingham, Utah, and the two concentrating mills, one at Magna and one at Arthur, Utah. The new trackage, known as the Copperton line, went into operation on May 1, 1948.

The general territory included in this electrification project is shown in Figure 1. The road traverses a rolling country through numerous cuts and fills, crossing over a main highway three times and several secondary highways at grade crossings. Figure 2 shows typical terrain.

Copperton yard, some seven miles below the mine, serves as an assembly yard for empty and loaded ore cars. The ore trains to the concentrators go down a maximum grade of 1.35 per cent to Arthur Junction from where they are sent to Fogarty yard serving the Magna concentrator, or to Arthur yard serving the Arthur concentrator, according to the needs obtaining at the time. A 2-mile lay of double track, Cyprus Siding, permits passing of loaded mill-bound trains and empty mine-bound trains. Power is fed to the trackage system at a point near the lower end of Cyprus Siding. Throughout the entire main line the curvature has been held to a maximum of three degrees because of the speed and length of trains handled. The trackage consists of 131-pound rails laid on hardwood ties. Ballast is crushed smelter slag which is readily available in this section of the country. Despite the proximity of this district to the Pacific northwest, there existed a shortage of fir for ties at the time of construction which dictated the use of hardwood despite its greater cost.

This electric railway is an entirely new project and completely replaces the original haulage system which lay to the west in a fairly rugged terrain. The old system had a ruling grade of 2.5 per cent and curves as sharp as ten degrees. Its motive power was coal-burning, Mallet-type locomotives weighing 350 tons and having a starting tractive effort of 117,000 pounds simple and 98,000 pounds when compounded. These locomotives normally handled a train of 46 cars at an average speed of ten miles per hour upgrade. The new system is designed to handle 75 car trains at a speed of 25 miles per hour.

The old and new locomotive characteristics are indicated in Figure 3.

The rolling stock used on the old system and transferred to the new system consisted of special-type gondola cars weighing 24 tons empty and 116 tons when loaded with ore and ready for the downhill trip to the concentrators.

In 1924 the concentrator yards at Magna and Arthur were electrified at 600 volts for "dumper" locomotives whose purpose was to break up the delivered ore trains into cuts of 14 or 16 cars and spot these cars in the dumpers where they were emptied, after which they were pushed out of the dumper buildings to run by gravity to the lower end of the empty yards. Under the 1948 electrification the concentrator yards were electrified at 3,000 volts and a single cab (1/2 of a complete locomotive) serves as a dumper locomotive, handling cuts of 20 cars. The 3,000-volt electrification necessitates the use of "reach" cars between the locomotive and the car dumper for two reasons. First, the locomotives are physically too large to enter the building, and secondly, in the interest of safety, it was decided to dead-end the 3,000-volt catenary system outside the dumper building. The old locomotives in dumper service were sent to the mine where they are now operating.

Copperton yard (Figure 4) is electrified at 750 volts to accept the mine locomotives. The 3,000-volt locomotives also operate at 750 volts within this yard. It may be well to point out at this time that empty trains leaving the concentrator yards start up-grade to the mine, hence require full power. This fact decided 3,000-volt power in the Arthur and Fogarty yards.

The present electrification is a continuation of a program started in 1923 to completely electrify the mine at Bingham, the world's largest open-cut copper mine, both as to loading and transportation. This program, which was completed in 1928, includes 42 electric shovels and 67 85-ton 750-volt electric locomotives. Approximately 150 track miles of overhead trolley construction is required to serve the mine alone, from which is transported a total of some 67,000,000 tons of ore and waste material annually to Copperton yard or to the waste dumps.

Power

Kennecott Copper Corporation generates its own power for its operations in Utah in a modern steam-electric station having an installed capacity of 100,000 kw. This power is generated at 13.8 kv which is transformed to 44 kv for distribution to the mine, railroad, and concentrators. The 44-kv power to the mine is transmitted over a 2-circuit steel tower line so routed as to go through the railway rectifier substation and there to supply power for the railroad.

Comparative economic studies made to determine voltage and current characteris-

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R. J. Corfield is with the Kennecott Copper Corporation, Utah Copper Division, Garfield, Utah.

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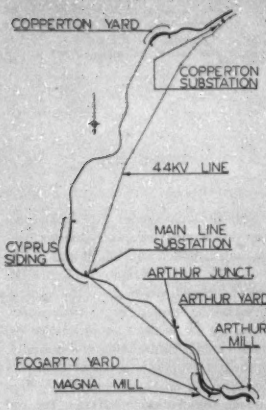

Figure 1. Map of trackage system and 44,000-volt transmission line serving the electric railway and mine

tics indicated that 3,000 volts of direct current was the optimum. Investigation of conversion apparatus dictated the selection of single anode rectifiers because of their adaptability to the high voltage selected, their efficiency, overload capacity, and low maintenance. The two rectifiers installed are believed to be the first of their capacity and voltage ever used for railway electrification in this country. They are each rated at 3,000 kw 3,200 volts, giving the station a nominal continuous rating of 6,000 kw. Figure 5 shows the general arrangement of the station. The control is fully automatic, metal-clad switchgear (Figure 6) with all instruments and adjusting devices facing inward to the control corridor so that an operator making a cursory inspection need not enter the rectifier and circuit-breaker rooms, which are ordinarily kept locked, as the rectifiers (Figure 7), rectifier auxiliaries, and high-speed circuit breakers (Figure 8) are all at a potential of 3,000 volts above ground. The automatic control used is conventional with the exception of the automatic transfer scheme for selecting either 44-kv line in case of a power failure on the line supplying the rectifiers.

The main line rectifier station is located at the economical load center, taking into account the spotting service of the 2-car dumper locomotives. This load center is not the geographical center, but lies somewhat closer to the concentrators than to the mine.

Two d-c feeders emanate from the rectifier station, each consisting of two 1,000,000 circular mils cables in transite conduit and terminated by means of potheads on a pole adjacent the station. These feeders are sectionalized at the pole, one feeder supplying the Bingham end, the other supplying the concentrator end, and are paralleled only when the locomotive passes under the trolley section break. Both rectifiers normally operate in parallel on the 3,000-volt bus supplying power to either, but not both, ends of the electrification. Such an arrangement is desirable in this particular case so that the load limiting and short-circuit detection equipment will function properly.

Power for the Copperton assembly yard at the mine, and the two feeder tracks to this yard, is supplied by two 750-kw 750-volt single anode-type rectifiers installed as a part of the mine haulage conversion equipment which consists of ten 1,000-kw 750-volt synchronous converters in addition to the two rectifiers. The part of the power system used for main line electrification is shown in Figure 9.

Catenary System

The catenary system used on this electrification was developed after a study of several proposed schedules of train operation, all of which took into account the operation of the two 125-ton car dumper locomotives. These were in spotting service on a $2^1/_2$-minute cycle, often with coincident peaks which were superimposed on the main line load. The maximum calculated peak was 2,600 amperes and it was desirable to limit the voltage drop to 15 per cent under this condition. Having determined the required conductivity, the problem became one of copper distribution, and, in the case at hand, a relatively light catenary was desirable as there are a number of deep cuts on this railroad and a derailment will require either shifting the overhead or removing it from over the center of the track. On the other hand, enough weight in the catenary system is required for stability.

The catenary finally selected consists of a 1/2-inch, 30 per cent conductivity, copperweld messenger; a number 4/0 stranded copper intermediate messenger and a 350,000-circular mils grooved trolley wire. The method of hanging the num-

Figure 2. The electric railway traverses a rolling country

This illustration shows a trainload of ore on its way downhill to the concentrators

Figure 3. Comparison of tractive efforts of steam and electric locomotives

Tractive effort for electric locomotives is for a complete locomotive of two units

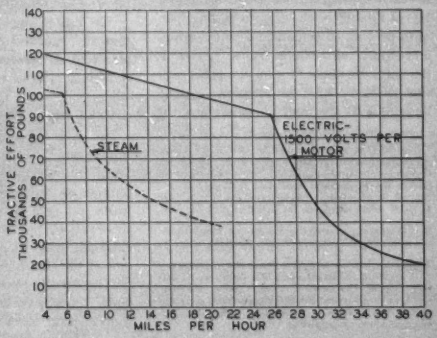

Figure 4. Copperton yard from the mine end

ber 4/0 messenger and supporting the 350,000-circular mils trolley is shown in Figure 10. Main messenger tension was kept to a maximum of 6,590 pounds under an assumed loading of 1/4-inch radial ice on all wires and a 12-pound wind transverse to all wires.

In all yards, where 15 miles per hour is the speed limit, simple catenary (Figure 11) is used. That is, the number 4/0 intermediate messenger is omitted. Conductivity is ample, due to the large number of tracks which are all in parallel.

In order to provide the necessary additional conductivity, the catenary system is supplemented by two 1,000,000-circular-mils feeders which are connected alternately to the catenary every 1,000 feet. Feeders and catenary are sectionalized at the substation and all yards.

Detailed consideration was given to the question of steel versus wood for the supporting structures, and, while steel was preferred, it could not be economically justified on this electrification due, primarily, to the fact that past experience indicates a life of cedar poles in this territory at 20 years or more. The poles supporting this catenary system are fully treated fir, commonly known as "Blackjacks," rather than butt-treated western red cedar which were not obtainable. While fir poles, properly treated, have a good reputation, it is our first experience with them, and time alone will tell as to their life.

Both catenary and feeders are supported on standard 10-inch suspension disks, there being two in series. The decision to use double insulation was predicated on two facts. First, this railroad is out in the wide open spaces, subject to considerable target practice, and it was felt that the shooting of one disk would still permit operation until repairs were made. Secondly, this line traverses a rather severe lightning section, and continuity of service requires that flashovers be minimized as much as possible.

In addition to the catenary and feeder system, these poles support a signal control cable and a 2,300-volt signal power line (Figure 12).

Catenary construction was done in the conventional manner. The main messenger was strung to tension and clipped in advance of the auxiliary messenger and trolley. These conductors were strung together by a first stringing crew and temporarily clipped to the main messenger by means of hook wires. A second crew followed with a clipping car having a working platform 40 feet long. This crew did the final clipping, off-setting, pullovers, and dressing.

All rail joints are bonded with 7-inch, number 4/0 copper bonds, gas-welded to the rail head. Frogs are bonded in the conventional manner, and 2,000-ampere impedance bonds are installed at all signal locations permitting both rails to be used for the return of propulsion current.

Substations

The purpose and characteristic of equipment decided the design of the substation buildings (Figure 13). They are as fireproof as possible with no wood or other combustible material being used in their construction. All doors are steel and set in steel frames. No windows are used, structural glass block panels provide the necessary light. This results in a relatively dust-tight building which is a distinct advantage to the operation of automatic switchgear components such as relays and contactors. The building walls are concrete and brick, floors are concrete slabs on steel frames, the roof is gypsum plank and specification roofing. The interior walls and ceiling are lime plaster on steel lath.

Since the heat exchangers, which are located in the basement, dissipate considerable heat, vertical ventilating ducts built into the building entrance structures take cold air from above the building through intake fans which discharge directly in front of the heat exchangers. These intake fans are thermostatically controlled. Heated air is rejected through outlet ducts in the opposite side of the building entrance structures or through openings in the floor to mix with other heated air from equipment installed on the main floor. This heated air rises

Figure 5. Floor plan of the main line substation

High-voltage components are in rooms separate from the control equipment room. Access to the basement is spiral stair in circuit breaker room

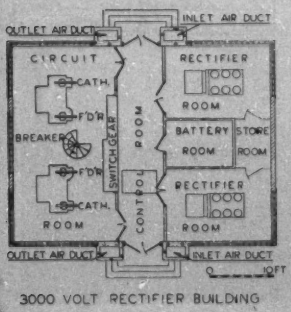

Figure 6. Control board as it appears from the control room

Grating in foreground covers hatchway for lifting equipment from basement

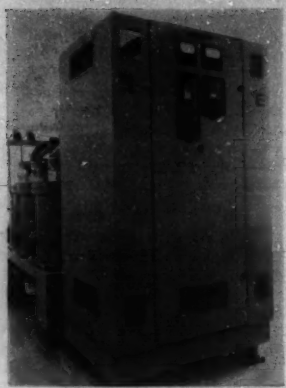

Figure 7. Rectifier in main line substation

Cubicle and rectifier work at 3,000 volts above ground, are set on insulators

Figure 8. High-speed, d-c, circuit breakers

Circuit breakers to extreme right and extreme left are on rectifier cathodes; other two are on lines to overhead system. Spiral stairway leads to basement

to the ceiling where grilled openings permit it to enter the space between the ceiling and the roof from where it is exhausted through a fan to the outside during warm weather and exhausts by chimney action during cold weather. The exhaust fans are manually controlled and run continuously during the summer months when outside temperatures reach a maximum of 100 degrees Fahrenheit. Roof drains are electrically heated to prevent damage to the roof from freezing water. All lighting in the station is fluorescent and lights are controlled by a timer. Most of the heat for the building is supplied by reject from the equipment, but 12 kw of supplementary heat is provided by electric convection heaters. The battery room is isolated to protect other equipment from corrosive or explosive fumes and has a separate ventilator discharging directly to the atmosphere. The degassing transformer (Figure 14) serves both rectifiers. High-voltage equipment in the basement is enclosed in wire screen.

The main rectifier transformers, station auxiliary transformer, and associated switchgear are of the outdoor type, and a conventional steel structure mounts the disconnecting switches, fuses, and lightning arresters. The entire outdoor yard area is mulched with an oiled gravel, graded for drainage and floodlighted from lights on the first towers adjacent the substation structure. The outdoor switchyard and the building are surrounded by a 6-foot fence on steel posts. All equipment mentioned in this paragraph is also shown in Figure 13.

Locomotives

The locomotives selected for this application are special in several respects. Of major importance was the fact that the duty cycle is rather severe due to the heavy trailing load up-grade and the speed required to maintain the tonnage between the mine and concentrators. It was also desirable to obtain as much electric braking as economically possible in order to reduce brake-shoe wear and promote safety. A thorough study was made of regenerative and dynamic braking. The final decision favored dynamic braking due primarily to the fact that rectifiers were to be used for a power source for this electrification. Inverted rectifiers, together with certain necessary locomotive auxiliaries, to permit regeneration, would have increased the first cost beyond the limit of economical justification.

These locomotives (Figure 15) feature all-welded construction. The trucks are exceptionally heavy, weighing approximately 40 tons each. The main deck plate is 2 inches thick and weighs approximately 20 tons. The cab structure and auxiliary equipment compartments are of welded-sheet steel and the cab is completely insulated. All compartment doors housing high-voltage control equipment are master keyed to prevent entrance of unauthorized personnel. All motors are equipped with antifriction bearings arranged for grease lubrication. Journal bearings are oil-lubricated by "felpax" wick lubricators with special attention to the journal thrust collar.

The articulated joint between the two trucks is of the ball-socket type, prelubricated and sealed against the entrance of foreign matter.

Two air compressors, each having a capacity of 75 cubic feet per minute, are installed in each locomotive cab.

Clasp brakes are used and air brake control is conventional except as modified to work in conjunction with the dynamic braking. This exception provides an electric interlock in the controller to prevent the application of independent air when the train is under dynamic braking but provides for train line brake operation from the locomotives.

Sanders are air operated and the piping so arranged that all wheels may be sanded in either direction.

The locomotive control is somewhat special due to the fact that the main-line locomotives use the mine-yard tracks thus requiring dual voltage control. Twelve 3,000/750-volt section breaks are installed at the lower end of Copperton yard to provide a 30-foot dead trolley section between the two systems. Main line locomotives will normally coast through the dead section with the master con-

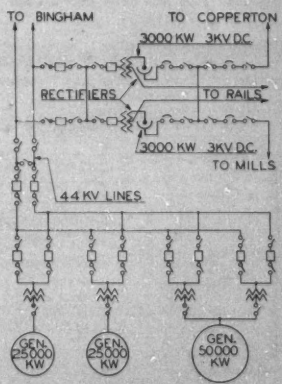

Figure 9. Electric railway power system

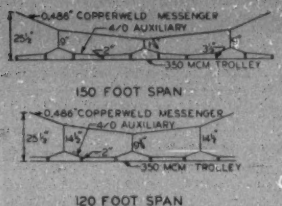

Figure 10. Typical catenary spans used on main line

troller returned to its "off" position before the power collectors are de-energized. A selector switch in the controller must be manually positioned to agree with the system voltage, either 3,000 or 750 volts, before power may be applied to the traction motors. Should the engineer fail to make the correct selection of system voltage, the locomotive high-speed circuit breaker opens when the controller operating handle is moved to an "on" position.

The control equipment provides 2-speed motoring operation of the four traction motors from either a 3,000-volt trolley or 750-volt trolley. With 3,000-volt power the traction motors are connected four-in-series to secure 1/2 speed and then are connected two-in-series and two-groups-in-parallel to secure full speed. Free-running speed in the first (series) combination of motors is reached by fifteen evenly graduated resistance steps, these being the first to sixteenth notches of the master controller. Free-running speed in the second (series-parallel) combination of motors is reached by 11 evenly graduated resistance steps, these are the seventeenth to twenty-eighth notches of the controller.

With 750-volt power the traction motors are connected "series-parallel" to secure 1/4 speed and then all four motors are connected in parallel to secure 1/2 speed. Free-running speed in the series-parallel connection is reached by 13 evenly graduated resistance steps. These are attained on the first to sixteenth controller notches. (Notches 1, 2, and 3 maintain the same resistance value and are the equivalent of a single step.) Free-running speed in the parallel connection is reached by nine evenly graduated resistance steps. These are attained on the seventeenth to twenty-eighth controller notches. (Notches 17, 18 and 19 maintain the same resistance value and are the equivalent of a single step.)

The power circuits to trolley or ground are established by 48 electropneumatic contactors, divided into two banks of 24 each. They connect and commutate the resistors for motoring and braking operation. A series-parallel switch consisting of 17 cam-actuated contactors establishes the three motor combinations, "series," "series-parallel" and "parallel." Two similar electropneumatic switches, a reverser and a motor brake switch make the connections between the armature and fields of the motors.

A 5-pole switch serves to cut out one pair of motors, either numbers 1 and 2 or numbers 3 and 4, in case of a motor or circuit failure so that the locomotive may proceed under the power of two motors. With one pair of motors out of service, the remaining pair cannot be used for dynamic braking. Four overload relays, each one with a series coil in the circuit of a motor, are built into the cutout switch. These act to open the high-speed circuit breaker in the trolley circuit.

Relays that are energized directly from the trolley control the connections of the blower and compressor motors to suit the imposed voltage. They also control the high-speed circuit breaker to force the selection of the 3,000-volt or 750-volt circuits according to the line voltage.

Energized from the 3,000-volt system, two relays pick up and lock out a third relay and set up the control circuits for high-voltage operation. Energized from the 750-volt system, these two relays will not operate, and thus they permit the third relay to be energized through the action of its time-delay contact. After approximately $1^1/_4$ seconds this third relay will close and set up the control circuits for low-voltage operation.

Each traction motor and associated accelerating resistor is force ventilated by a separate blower. A 1,500-volt motor in each end cab drives four blowers, two for the traction motors and two for the resistors located at that end of the locomotive. Each of these motors also drives a 75-volt generator for charging the battery used for control and lights.

The two 1,500-volt blower motors are operated in series from the 3,000-volt line and in parallel from the 750-volt line. On 3,000 volts they start when magnetic contactors close under the control of the trolley relays. Two resistors limit the starting current on 3,000 volts and are shorted out by contactors under the control of relays when the motors reach approximately 1/2 speed.

On 750 volts they start when contactors close under the control of the third trolley relay. The starting resistors are not in circuit at this voltage. The blowers must run in order that the high-speed circuit breaker may be held closed and that the compressors may operate.

Figure 11. Typical catenary construction used in yards

Note that number 4–0 auxiliary messenger is not used

Figure 12. Typical single-track tangent construction

Note two feeder cables suspended from smaller brackets. Multiconductor signal cable is fastened directly to poles

Figure 13. Main line substation

Entrance structure contains ventilating ducts to basement and attic exhaust fan duct

Two 1,500-volt compressor motors are operated in series from the 3,000-volt line and in parallel from the 750-volt line. The voltage across each of the series connected compressor motors is maintained approximately equal while the machines are above 1/2 speed by a connection between the mid-points of the blower and compressor motors. This provides a successful running condition but, to insure that both compressors will start, the voltage imposed on them is further equalized during the starting period by paralleling them with equalizing resistors.

These resistors are open circuited by contactors when counter electromotive force relays respond to the counter electromotive force of their respective machines. If one machine speeds up more than the other, the relay of the faster one, at approximately 1/2 speed, will take out the equalizing resistor connected across the slower motor causing an increase in its current, torque, and speed.

On 750 volts the starting resistors and equalizing resistors are not in circuit.

A cutout switch located in the operating cab control compartment provides a means for open circuiting the compressors in case of a fault in a compressor or its circuit. This permits multiple-unit operation of two locomotives, one with its compressors cut out in that class of service where the remaining two compressors can supply a sufficient amount of air.

Dynamic braking is arranged to develop maximum braking effort at approximately 24 miles per hour. A series of nine braking notches are provided, these being effective between 9 and 27 miles per hour without exceeding the motor rating or depending upon adhesion greater than 18 per cent.

The control equipment provides dynamic braking operation by disconnecting the motors from trolley power and using them as generators driven by the locomotive and dissipating the generated power in resistors. The motors are separated electrically into two groups and then the two motors of each group are paralleled with their fields transposed by means of a switch for purposes of stability and connected to a pair of accelerating resistors. Nine braking steps are obtained by commutating the paralleled resistance. Dynamic braking can be secured only when 3,000-volt power is available to produce full, forced ventilation of the resistors.

All regular control features, including dynamic braking and voltage selection are embodied in a single control stand. Cab interior is shown in Figure 16.

Communications

This railroad is equipped with the most modern types of signal and communication systems. A control board, installed at Copperton yard and operated by one operator on each shift, controls all train movements in and out of Copperton yard, all main line signals and switches, including two crossings of the main line by other railroads, and the switches and signals at the approaches of both concentrator yards. The control board is circuited by two types of systems. That part governing the Copperton yard is typical "NX" (eNtrance and eXit) interlocking which uses a unit wire control for each function. That portion controlling the main line, passing track and concentrator-yard approaches makes use of circuits whose relays operate by coded pulses. Where foreign roads cross the main line, electric locking and approach indication is provided and is under the control of the operator at Copperton. The passing track signals at Cyprus Siding are provided with a "Take Siding" indication since the schedule is so arranged that passing meets generally will be made within this 2-mile double track section.

All switches are operated at 24 volts and all signals at ten volts, the power being taken from batteries located at the operating device. Signals are colored light type. Dwarf signals (Figure 17) govern all the electrically-operated switches in the yards, while those controlling traffic on the main line are mounted on high masts.

Self-supporting aerial cable is used for control between the control tower and the signals and switch machines. This cable is multiconductor, there being 9, 15, 22, or 37 conductors, depending upon the location and number of functions to be controlled. Conductors are number 14 American wire gauge solid copper. This cable is supported on the catenary poles along the railroad.

All switches are electrically heated by tubular snow melters rated at 6 or 12 kw depending upon the length of the switch. These heaters are under the control of the operator at Copperton. Power for the heaters is furnished from the main line substation at 2,300 volts

Figure 14. Degassing transformer serves both rectifiers

Heat exchangers and degassing links are in fenced enclosures at left and right

Figure 15. A single main-line locomotive is made up of two 125-ton cabs

Both locomotives are operated from a single cab

Figure 16 (left). All electric controls are in a single controller

Figure 17 (right). Dwarf-light signals control traffic in the yards

An electric switch machine is shown in the foreground

which is transformed to 220 volts at the heater terminals. For charging signal batteries, 110-volt power is used from transformers on the same 2,300-volt line.

To supplement the signals, ultra-high frequency radio in the 152 to 162-megacycle band will be installed to provide telephone communication between the Copperton control tower operator, five fixed land stations and three mobile automobile sets. These automobile sets are for the use of the superintendent, signal supervisor and the maintenance of way supervisor. Railway mobile units are to be provided for all locomotives and cabooses to provide end to end communication, since a made-up train is approximately 1/2 mile in length, which practically eliminates hand or lantern signals even in fair weather. Connections are also provided for radio communications between the tower operator and train crews if necessary.

Conclusion

This electrified railway has not been in service long enough to obtain any definite operating data. However, results to date indicate that the calculated values as to power consumption, scheduled speeds, acceleration, dynamic braking, and general design of the entire project are more than adequate to meet the operating conditions proposed to the engineering department in the original premise.

No Discussion

The "Linascope"—An Echo-Ranging Type Fault Locator for High-Voltage Lines

J. R. LESLIE
ASSOCIATE AIEE

K. H. KIDD
ASSOCIATE AIEE

Synopsis: The "Linascope," an electronic fault locator is described which uses an echo-ranging method for determining the location and nature of transmission line faults. This device has been used on open-wire telephone circuits for the location of various fault conditions, including open and short circuits, and high-resistance joints. Tests have been performed on high-voltage transmission lines which were removed from service for the purpose. Artificial faults applied at distances as great as 300 miles were detected. Open and short circuit faults are readily observed. Transposition points give slight echoes. The equipment has been used for inspecting live lines. In this application a high-frequency pulse is used which is coupled to the line by means of a high-voltage coupling capacitor. It is intended to use photographic recording gear with this instrument for the continuous inspection of live lines, in order to locate transient faults such as occur with lightning flashover of live-line insulators.

THE inspection of circuits by pulse reflection methods is well known to communications engineers. In particular, this technique has been used in inspecting high-frequency coaxial telephone and television lines. A pulse-type fault locator was developed for the United States Army for locating faults on field quad cables.

More recently attention has been devoted to the application of this method for locating faults on power lines.

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J. R. Leslie and K. H. Kidd are both with the Hydro-Electric Power Commission of Ontario, Toronto, Canada.

When a voltage pulse of short duration is applied to the terminals of a transmission line, the wave travels along the line at a velocity which depends upon the line parameters. The wave velocity in cables ranges from 0.5 C to C where C is the speed of light (0.186 mile per microsecond). For all open-wire lines the wave velocity is constant for practical purposes and very nearly equal to the speed of light.

As the wave travels along the line it suffers some attenuation or loss in amplitude due mainly to the resistance of the conductors. There is also some distortion of the wave shape due to the nonuniform attenuation and delay of the frequency components of the wave. This distortion is very pronounced on ordinary telephone cables which have a nonuniform frequency response. On open-wire lines a voltage pulse travels with negligible distortion and only slight attenuation. The attenuation factor due to the resistance of the conductors is $\exp(-Rx/Z)$. Assuming a 4/0 copper conductor of surge impedance $Z=500$ ohms and a radio frequency resistance at one megacycle of $R=5.4$ ohms per mile, the foregoing formula indicates that a pulse must travel 130 miles to attenuate to one half its original value.

It should be noted that this paper deals with pulses of relatively small amplitude (1,000 volts or less). For lightning surges the attenuation is of course much higher, due to the presence of corona losses.

If at any point along the line there is an abrupt change in impedance then a portion of the incident wave is reflected back toward the source while the remainder of the wave continues in its original direction. Table I shows some of the more important impedance discontinuities which occur on transmission lines, together with formulae giving the magnitudes of the reflected and transmitted pulses.

It will be noted that at an open-circuit fault the echo is of the same magnitude and polarity as the incident wave. At a short circuit fault the echo is again of the same magnitude but is reversed in polarity. If the line is terminated in its characteristic impedance there is no reflection from the termination. In general, if the pulse is incident upon an impedance greater than that through which it is passing, an echo arises of the same polarity. If the pulse is incident upon an impedance lower than that through which it is passing, then an echo of reversed polarity occurs.

The fault locator described in this paper develops a short duration (approximately 25 microseconds) voltage pulse which is connected to the line under test. Echoes returning from any faults on the line as well as those from major impedance discontinuities are displayed on a suitable time base on a cathode ray tube. The pulse is generated repetitively in order to obtain a steady visual pattern on the cathode ray tube.

The time required for the return of any particular echo is then measured and hence the distance to the source of that echo is determined. The magnitude and polarity of the echo give information regarding the nature of the source of the echo. The choice of the cathode ray tube sweep and time measurement circuits are influenced by much the same factors that arise in their use on radar equipment. With the large number of precision circuits that are available, practically any desired degree of accuracy can be achieved in the measurement of the echo distances.

Portable Linascope Fault Locator

The considerations previously described led to the construction of a portable pulse-type fault locator. Since similar units

Table I. Typical Transmission Line Discontinuities with Formulae for Echo Magnitudes

Open Circuit Fault $Z=\sqrt{\frac{L}{C}}$	Incident ..... E_I Reflected E_R ..... E_I Transmitted E_T 0	Echo Same Polarity
Short Circuit Fault $Z=\sqrt{\frac{L}{C}}$	Incident ..... E_I Reflected E_R ..... $-E_I$ Transmitted E_T 0	Echo Reversed in Polarity
Line With Shunt Resistance $Z=\sqrt{\frac{L}{C}}$ R	Incident ..... E_I Reflected $E_R = -\frac{Z}{2R+Z}E_I$ Transmitted $E_T = \frac{2R}{2R+Z}E_I$	When $R = 10{,}000\ \Omega$ $Z = 500\ \Omega$ $\frac{E_R}{E_I} = -2.5\ \%$
Line With Series Resistance $Z=\sqrt{\frac{L}{C}}$ R	Incident ..... E_I Reflected $E_R = \frac{R}{R+2Z}E_I$ Transmitted $E_T = \frac{2Z}{R+2Z}E_I$	When $R = 10\ \Omega$ $Z = 500\ \Omega$ $\frac{E_R}{E_I} = 2.0\ \%$
Change in Line Spacing $Z_1=\sqrt{L_1/C_1}$ $Z_2=\sqrt{\frac{L_2}{C_2}}$	Incident ..... E_I Reflected $E_R = \frac{Z_2-Z_1}{Z_1+Z_2}E_I$ Transmitted $E_T = \frac{2Z_2}{Z_1+Z_2}E_I$	Typical Transposition Gives $\frac{E_R}{E_I} = \pm 2.5\ \%$ Testing Phase to Phase
Terminated Line $Z=\sqrt{\frac{L}{C}}$ R	Incident ..... E_I Reflected $E_R = \frac{R-Z}{R+Z}E_I$ Terminal $E_T = \frac{2R}{R+Z}E_I$	No Echo When $Z = R$

have been described elsewhere this one will not be covered in detail. The Linascope is the same size and has the outward appearance of a conventional cathode ray oscilloscope. A 5-inch cathode ray tube is used to display the echo pattern and the usual focus, intensity, positioning and gain controls are associated with it. Two terminals are provided at the rear of the instrument from which connections are made to the line under test. A switch on the panel provides various pulse amplitudes from 1/10 volt to 100 volts. A small pulse amplitude is used on telephone lines, since an undesirable clicking noise is otherwise created in adjacent circuits. However, on power lines the 100-volt pulse is usually used. A range switch provides ranges of 50, 100, 200 and 300 miles by appropriate choice of the sweep velocity. The equipment is protected against accidental surges from the line under test by means of a spark gap and a 2,500-volt 1-ampere fuse across the line terminals. Parallel-T filters are provided across the line terminals to prevent interference from power frequency voltages.

When locating a fault on a power line, the line is first de-energized and grounds are applied at the far end as a safety precaution. The Linascope is then directly connected to the line. There is a slight advantage in connecting both Linascope terminals to phase conductors, but one terminal is usually connected to a phase conductor and the other to a good ground (towerleg, and so forth). When the Linascope is turned on, the outgoing pulse appears as a vertical pip at the left side of the cathode ray sweep. The echo from the far end of the line usually appears toward the right end of the trace. The echoes from one or more faults appear at intermediate points along the trace. The distance to a fault is measured by using a calibrated mileage scale laid along the face of the cathode ray tube. The known distance to the end of the line can be used as a check.

It will be noted that the distance measurements are limited in this instrument to the accuracy with which one can read a 5-inch cathode ray screen. In practice this leads to a precision of $\pm 1/2$ per cent of the full scale range. Much greater accuracy can be obtained by using an expanded sweep circuit in which a limited portion of the complete scale is selected, amplified to the full screen width, and examined in greater detail. The unit described before has, however, sufficient accuracy for use under field conditions and has a great advantage in terms of simplicity and ease of operation.

Transient Fault Locator

The fault locator was designed primarily for use on relatively long telephone

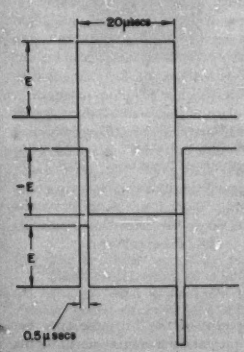

Figure 1 (left)-Echo arising from fault near the test terminals

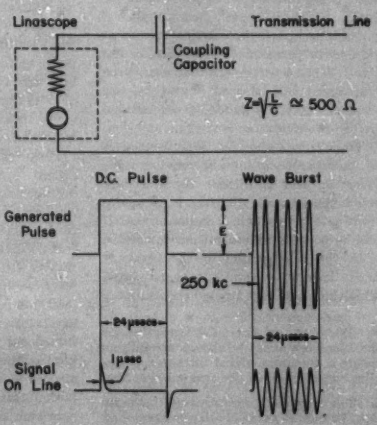

Figure 2. Passage of d-c and wave burst pulses through coupling capacitor to transmission line

and high-voltage transmission lines in locating faults which cause permanent outages until repaired. Tests which will be described later have proved that the unit is able to locate practically any fault within this category.

No tests have been made with the unit on distribution networks as yet. Most distribution networks have so many interconnections that the echo pattern will be quite complicated. It probably will prove necessary to have a chart of the circuit under normal conditions, with which the pattern under the faulted conditions can be compared. Even then, the possibility of several paths of the same length will have to be considered. It is doubtful, too, that these units can be made available in sufficient quantities within a power company, for use as a routine maintenance instrument on distribution circuits.

In performing tests on high-voltage transmission lines, speed in the location and clearing of a permanent fault is all important. Another type of fault which is a more common problem to the system operator is the transient fault. Lightning often flashes over a line insulator, and the subsequent power flow causes a momentary interruption of service. When the circuit breakers reclose, the insulator is sometimes able to sustain the line voltage. The insulator is left in a damaged condition and represents a potential source of further trouble unless located and replaced.

The development of the Linascope has continued with the construction of a unit which may be coupled to a high-voltage transmission line via a high-voltage carrier-type coupling capacitor, rather than being connected directly to a phase conductor. This development makes it possible to install the Linascope in a station, with permanent connection to the line. When a permanent fault occurs the instrument can be used immediately to locate it, avoiding the delay necessitated by direct connection to the line.

The ultimate objective with this unit is to incorporate automatic photographic recording gear and to run the Linascope continuously while the line is in operation. With this unit it is hoped that it will be possible to locate transient faults.

In the construction of the transient fault locator, several problems arise, which have not been treated previously.

1. The shape of the pulse generated by the Linascope must be chosen carefully to ensure a maximum signal through the coupling capacitor on to the transmission line.

2. The frequency spectrum of the pulse must be such as to avoid interference to any carrier power line equipment installed on the same line.

3. The Linascope receiver circuits must be designed to avoid interference from the carrier transmitter, and from any radio frequency noise voltages present on the line due to corona.

4. Any radio frequency radiations from the line must be below tolerable limits set by the authorities.

The pulse commonly used when making connections directly to the line is a unidirectional voltage surge of short duration. In choosing a suitable pulse duration, two factors must be considered.

1. The pulse duration sets the minimum distance from which echoes may be received without interfering with the outgoing pulse. That is, if the pulse is too long, then the echo from a nearby fault will start to return before the outgoing pulse has finished.

2. A narrow pulse contains frequency components over a broad band and hence the pulse width must be chosen with due regard to the available transmission band. A handy rule-of-thumb is that a rectangular voltage pulse T microseconds long contains important frequency components up to $1/T$ megacycles.

In the portable Linascope, the authors chose a pulse width of 25 microseconds as a suitable compromise. Since the pulse travels at a rate of approximately 0.1 circuit mile per microsecond, it is seen that echoes from faults closer than 2.5 miles will interfere with the outgoing pulse. However, upon one occasion a fault was located only 250 feet from the test terminals. The line under test was a telephone circuit some 40 miles long, which had a short circuit fault. Upon inspecting it with the Linascope, the pattern shown in Figure 1 was seen. Consideration of the pattern shows that the fault was 0.5 microsecond away or, roughly, 250 feet.

Other workers have shown that the band width can be minimized for a given pulse width by suitably shaping the pulse, but the authors have found that the choice of neither pulse shape nor duration are critical, when the unit is directly coupled to an open-wire line.

However, when the Linascope is connected to an open-wire line via a high-voltage coupling capacitor as is required for a transient fault locator, the unidirectional pulse has several undesirable features. The carrier type coupling capacitors in general use on high-voltage lines have capacitance values ranging from 0.0005 to 0.002 microfarad. A high-voltage transmission line has a characteristic impedance of about 500 ohms. The internal impedance of the Linascope is also set at about 500 ohms for maximum power transfer and the prevention of near-end echoes. Thus the equivalent circuit of the terminal connection is as shown in Figure 2.

It will be noted in the figure that the rectangular pulse is transformed into two very narrow pulses of opposite polarity, each being about one microsecond in duration. Thus it is impossible to develop a pulse of longer than one microsecond duration on the line. This pulse contains important frequency components up to one megacycle and it is feared that it will give undesirable interference to carrier frequency equipment in the 100 to 200 kc band In addition, the Linascope receiving amplifiers must be designed to cover this wide band and will, in turn be subject to carrier interference and radio noise from line corona.

These considerations led to the study of the other pulse shape of Figure 2. This pulse consists of a short duration burst of radio frequency voltage. A burst frequency of 250 kc has been used and a burst duration of about 25 microseconds. An analysis of the circuit of Figure 2, substantiated by tests on transmission lines have shown that the burst passes through the coupling capacitor without appreciable change in shape. Furthermore, by using a series tuning inductor at the base of the coupling capacitor it is possible to tune out the loss due to the coupling capacitor reactance and obtain the full output voltage on the line.

By suitable choice of the width of the burst and by shaping it slightly, it is possible to minimize the interference created by the burst in the transmission line carrier frequency channel.

A live-line Linascope was constructed in June, 1947, using the wave burst pulse. Experiments which are described later have shown that it can be coupled to a ive line via a high-voltage coupling capacitor as anticipated.

It is planned to fit the live-line Linascope with automatic photographic recording gear and to install it on a high-voltage circuit in the near future. The line that will probably be selected for this purpose is the Leaside–Beauharnois 220-kv circuit, which is 303 miles in length.

This line is fitted with Marconi carrier equipment operating at 172 kc and having a band width of 11 kc. Laboratory experiments show that the wave burst develops sufficiently low interference over this band. Work is proceeding with the design of filters to eliminate carrier interference to the Linascope.

Tests

Equipment of the d-c pulse type described previously was constructed by the

authors in September, 1946. The first tests were performed at that time on the 220-kv circuit of the Hydro-Electric Power Commission of Ontario which supplies power from Beauharnois generating station on the St. Lawrence River, to Leaside near Toronto. The total length of this line is 303 miles. The line was removed from service for the tests and all phase conductors were grounded at Beauharnois. The Linascope was connected between the red phase conductor and ground and the echo pattern observed on the cathode ray tube. An echo was observed from the ground connection at Beauharnois. A series of staged faults were then applied at Martintown and then at Bewdley 247 miles and 67 miles from Leaside, respectively. Red phase to tower, red phase to blue, and blue phase to white faults were simulated, the connections to the phase conductors being made with ground clamps. In each case the fault echo was visible on the Linascope.

It will be noted that there is sufficient coupling between the 3 phase conductors that a fault between white and blue phases is visible even when testing from red phase to ground.

The lines under test ran for the first 20 miles along a right-of-way shared by two other 220-kv lines which were alive at the time. The filtering circuits which are included in the Linascope proved adequate to remove any power frequency interference on the cathode ray tube.

Following these tests the d-c Linascope was assembled in a portable self-contained unit. This unit has been used extensively in the maintenance inspection of open-wire telephone lines operated by the commission. Not only has it been useful in locating open and short circuit faults, but it has also proved sufficiently sensitive to locate such minor faults as high resistance joints impeding but not preventing telephone communication and tree branches fallen across the telephone wires.

On July 26, 1947, tests were made for the first time with the waveburst type of pulse using two carrier frequencies, 250 kc and 1 megacycle. Tests were made also with the d-c type pulse for comparison. These tests were made on a 110-kv wood pole line running from St. Thomas to St. Clair, a distance of 66 miles. It was found that the echoes from the far end of the line as well as from artificial faults applied at an intermediate point were readily visible with both the d-c and waveburst pulses. The Linascope was then connected to the line under test via a high-voltage coupling capacitor. As was expected, it was found that the d-c pulse was considerably reduced (over twenty-fold) in magnitude after passage through the capacitor, whereas by suitable tuning it was possible to obtain waveburst echoes with only slight loss in amplitude. With the 1 megacycle carrier bursts, echoes were apparently set up by each pole structure showing up as a continuous interference pattern over the entire length of line. Grounds at the far end of the ine were nevertheless visible.

Experiments were then made to determine the operating limitations of the Linascope. For this purpose a new line of 220-kv construction from Barrett Chute to Oshawa was well suited. This line was, at the time of the tests, completed over a distance of 54 miles, to a point near Bannockburn, but not yet in service. Tests were made from Bannockburn with all phase conductors earthed at Barrett Chute and with faults of various resistance values applied at Mazinaw Lake, 23 miles distant.

Some of the records from this test are shown in Figure 3. It will be noted that open and short circuit faults are clearly visible, and that in each case an echo from the short circuit at Barrett Chute also appears. These oscillograms are reduced from the 5-inch scale of the cathode ray screen. Figure 3(e) was taken with no fault at Mazinaw and with increased vertical amplification. This record is faulty in that the normally straight horizontal sweep is warped due to overloading of the receiving amplifiers. Nevertheless the relative amplitude and directions of the transposition echoes are indicated correctly.

It is interesting to note that the transposition echoes occur in triplets, giving a short-circuit echo, and open-circuit echo, a very slight echo and then repeating. This sequence arises because the impedance decreases, increases, and undergoes negligible change at three successive transpositions.

The practical limit in Linascope sensitivity was taken to be that fault resistance at which the echo was reduced to approximately the same magnitude as those arising from transpositions. It was found that this condition is reached when a phase-to-phase fault as high as 10,000 ohms is applied or when a phase conductor is cut and a resistance as low as 10 ohms is inserted. From these tests the following conclusions were drawn:

1. The Linascope will detect broken (open-circuited) phase conductors.

2. A phase conductor which is short circuited to another phase conductor, to a tower or to a sky wire can be detected so long as the contact resistance is less than 10,000 ohms.

3. The Linascope is not suitable for locating high resistance joints on power lines. However, joint resistances of 10 ohms and

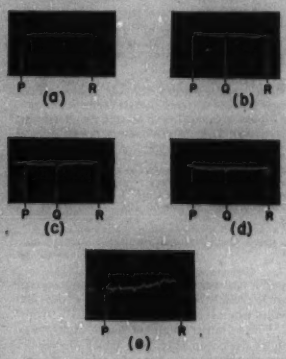

Figure 3. Records of d-c Linascope tests at Bannockburn (P) to Barrett Chute (R) (54 miles) with faults applied at Mazinaw (Q) (23 miles) 220-kv single circuit construction

(a) No fault. Linascope connected blue to white
(b) Red phase open circuited. Linascope connected red to white
(c) Blue to tower fault, zero resistance. Linascope connected red to white
(d) Blue to white fault, 1,000 ohms resistance. Linascope connected blue to tower
(e) Same as (a). Gain increased to show transpositions

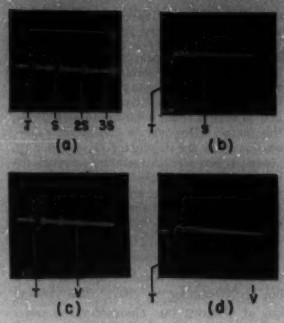

Figure 4. Records of live-line tests at Oshawa (T) showing echoes from Scarborough (S) (28 miles) and Sydney (V) (66 miles)

(a) Scarborough line energized, wave-burst pulse
(b) Scarborough line energized, d-c pulse
(c) Scarborough line open, wave-burst pulse
(d) Scarborough line open, d-c pulse

higher do occur on telephone lines and these are detectable with the linascope.

4. The case of a phase conductor fallen to the ground but not broken has not been tested. The resistance to ground is probably higher than ten thousand ohms in most cases and so only a small echo would arise from this factor. However, a considerable discontinuity in the conductor capacitance to ground is introduced, certainly as much as is created at a transposition. Therefore it seems reasonable that an echo of about the same size as that from a transposition should be seen.

The experiments outlined previously paved the way for the first tests on a live line on November 21, 1947. The 110-kv bus at Oshawa was chosen for this purpose. There is one 110-kv circuit running east from Oshawa, the nearest transformer station being at Sydney 66 miles away, and another 110-kv line running to Scarborough 28 miles to the west. There is no carrier equipment on these circuits. The linascope was connected to the base of a 0.001-microfarad coupling capacitor normally used for potential indication at the station. The apparatus at the base of the capacitor was replaced by the waveburst tuning unit.

It was found that echoes could be seen and identified from an open circuit at Scarborough some 20 miles away and from the bus connections at Sydney 66 miles away. It had been feared that radio frequency noise due to corona loss on the 110-kv line would interfere with the Linascope trace, but no difficulty of this sort was encountered even though the carrier pulse was less than 50 volts in magnitude. Two of the oscillograms taken during these tests are shown in Figure 4. It will be noted that the waveburst echoes are symmetrical about the horizontal axis. It is not possible to distinguish open-circuit echoes from short-circuit echoes without expanding the sweep in order to examine the polarity of the initial cycle of the echo burst. This is not usually practicable but the nature of the source of the echo is normally known from other considerations. The upper scale on the waveburst oscillograms is a timing wave having a period of 57 microseconds. With the Scarborough tie line energized, Figure 4(a), the echo from the open circuit at that point is clearly visible. A second and a third echo from Scarborough, marked *2S* and *3S* are seen also. The first echo upon returning to Oshawa has again been reflected by the bus connections at that point and has made the return trip to Scarborough two more times.

When the line to Scarborough was opened, the echo from Sydney 66 miles away then became visible. Sydney transformer station was in normal operation at the time and the echo must be due to the combined effects of switching structure, potential coupling capacitors and bushing capacities. The other echoes on Figures 4(a) and 4(c) have not yet been identified.

The d-c Linascope was connected also through the coupling capacitor to the live line. The results of these tests are shown in Figures 4(b) and 4(d). Small but sharp echoes are visible from Scarborough and Sydney respectively. The large negative "tail" on these records is again due to overloading of the Linascope amplifiers.

Conclusions

An electronic echo-ranging fault locator utilizing a unidirectional or d-c pulse has been described. Tests and operating experience have shown this unit to be a useful instrument for locating faults on open-wire telephone and transmission lines. The condition of the line under test is presented graphically on a cathode ray tube in a manner which can be interpreted easily, and one or more faults can be located with an accuracy which is entirely adequate for field use. Tests to determine the operating limitations of the d-c Linascope show that it is capable of locating any fault causing a permanent outage on an open-wire line. The unit is not sufficiently sensitive to locate high-resistance joints on transmission lines.

Progress in the development of a transient fault locator has been noted. The echo pattern of a live 110-kv line has been studied by coupling the Linascope through a high-voltage coupling capacitor. A waveburst has been considered as an alternative to the d-c pulse. The waveburst suffers a disadvantage in that it fails to distinguish the polarity of an echo clearly. However, the waveburst frequency spectrum can be chosen to minimize interference to carrier equipment on the same line.

Design and construction of the photographic recording equipment for the transient fault locator is proceeding and it is planned to make a trial installation in the near future.

References

1. United States Patent 2,345,932, **K. E. Gould.** 1941.
2. Transmission Line Fault Locator, **W. G. Hoyle.** *Report PRA135*, National Research Council of Canada, (Ottawa, Canada) September, 1946.
3. New Methods for Locating Cable Faults, (Part III) **F. F. Roberts.** *Journal*, Institution of Electrical Engineers, (London, England) November, 1946.
4. Pulse Echo Measurements on Telephone and Transmission Facilities, **L. G. Abraham, A. W. Lebert, J. B. Maggio, J. T. Schott.** AIEE Transactions, volume 66, 1947, pages 541–8.

Discussion

F. Noakes, D. J. Evans, and T. K. Naylor (The University of British Columbia, Vancouver, Canada): The work of Leslie and Kidd is of particular interest to us at the University of British Columbia since we have under development echo-ranging fault locators for aero transmission lines and power cables.

With reference to overhead lines, several techniques such as those employing d-c pulses, self-generated surges, and the continuous monitoring with radio-frequency bursts were considered. However, it appeared to us that a damped sine wave of approximately 200 to 500 kc would offer the best solution. In the fault locator under development, the signal monitors the line only during the period of power follow-up current in a flashover.

It is our opinion that photographic methods, as sole method of recording, should be discounted because of the delay in developing the film. We feel the photographic recording should be supplemented by a second tube having an exceedingly long memory. This would provide immediate information for the operators.

In some ways, the location of faults in power cables presents more difficulties than the location of faults on transmission lines. For example the time intervals are much shorter, the surge impedance of cable is about one-tenth of that of an overhead line and the attenuation is greater.

Briefly, the apparatus developed at the University of British Columbia consists of a low-voltage impulse generator combined with a timing device for measuring the interval between the transmission of the initial pulse and the reception of echoes from the discontinuities in the cable. The input pulse approximates a 0 – 0.2 microsecond wave which reaches a peak of about 500 volts when feeding a 50-ohm line.

The impulse generator has a high-power *5C22* hydrogen thyratron which discharges a capacitor directly into the cable. A compact 2-kv power supply recharges the capacitor while the echoes are being received.

The timer or marker pips of the echo pattern are compared on a Cossor split-beam oscilloscope adjusted to produce a single sweep each time a pulse is sent into the cable. As the marker pips, common sweep, and initial pulse are synchronized within 0.002 microsecond of one another, small parts of the trace can be expanded without affecting the relationship between the marker pips and the echoes. Hence the starting of the high-speed sweep can be delayed whenever echoes from distant parts of the cable are to be examined in detail. The

apparatus, exclusive of the oscilloscope, weighs about 37 pounds and consumes 175 watts.

Laboratory tests on *RG8U* polyethylene coaxial cable with a 100-ohm fault, 70 feet from one end, show that the distance to the fault may be determined with an error of considerably less than 1 per cent. Tests have also been made on a 3-conductor oil insulated paper power cable 1,389 feet long with a tap 484 feet from the sending end with the same order of accuracy. Investigation of the power cable shows that several miles of cable may be examined for faults and that attenuation is not too serious.

L. R. Spaulding (Bonneville Power Administration, Operations Branch, Portland, Oreg.): Leslie and Kidd are to be complimented upon their work on the "Linascope." They have contributed greatly to the development of an ultimate design of a fault locator which should incorporate all of the best features of all workers in the field.

At the communications laboratory of the Bonneville Power Administration, in addition to development work on the fault generated surge type of fault locator,[1] we have recently experimented with the application of a single, 1-microsecond 10-kv, d-c pulse to a 230-kv line, through carrier coupling capacitors, during fault conditions. The application of the pulse was delayed approximately two cycles (of 60 cycles), and the reflections from it were photographed from a 5-inch cathode ray tube presentation as a single sweep transient. Also, the subject of coupling to a live line, through carrier coupling capacitors in various combinations, is being investigated.

The work on this method of fault location, together with coupling experiments, is not completed, but, upon completion, might provide material of sufficient importance for a formal paper at some future date. However, the limited number of tests to date have been very encouraging and seem to indicate the following preliminary conclusions, which, we hope, can be fully verified in the near future:

1. The single pulse method will not require the assignment of a carrier channel nor in general will it seriously interfere with existing carrier channels.

2. A single 10-kv, d-c pulse of short duration is easier to obtain than a radio-frequency pulse of the same order of magnitude.

3. A delay of two cycles (of 60 cycles) from the start of a fault seems to be sufficient to permit a 10-kv, d-c pulse to over-ride residual steep wave front voltage wave distortions.

4. Within a two cycle period, a single delayed pulse and the necessary receiving equipment can be switched to the faulted phase, or to one of the faulted phases in the event of a line to line fault, by use of information supplied from conventional transmission line relay equipment, during fault.

5. Switching to the faulted phase or one of the faulted phases appears necessary because better reflections and less attenuation of the signal is experienced this way.

6. Two instead of three carrier coupling capacitors will provide adequate coupling to a 3-phase line by paralleling and pulsing the two coupled phases when the third phase is grounded.

REFERENCE

1. A TRANSMISSION LINE FAULT LOCATOR USING FAULT-GENERATED SURGES, R. F. Stevens, T. W. Stringfield. AIEE TRANSACTIONS, volume 67, 1948 (PROCEEDINGS T8308).

R. F. Stevens (Bonneville Power Administration, Portland, Oreg.): When we at the Bonneville Power Administration (BPA) first started work on this problem of fault location, we did not know what other investigators might be working in the field. However, we felt that the possibilities of applying modern electronic techniques would not long be overlooked. This supposition has been borne out by two papers on the subject presented at the Paris International Conference on Large High-Voltage Electric Systems,[1,2] and by this very excellent paper by Leslie and Kidd.

We are particularly interested in this last paper because the authors have not contented themselves, as some investigators have done, with the location of sustained faults, but have tackled the much more difficult problem of transient fault location.

The paper does not report operating results of the equipment in locating transient faults. I understand that this is because they have not had any such faults on the line to which the equipment was connected. We at Bonneville have gained considerable experience from staged fault tests, in which a line to ground arc was initiated by pulling a fine copper wire into the line by means of a piece of glass twine. Fortunately, the loop arrangement of the BPA 230-kv lines makes such tests possible at one point on the system, even under the present heavily loaded conditions.

The matter of coupling is very important and we would welcome more detailed information in this regard. Is one phase only pulsed, are all three pulsed in parallel, or is the equipment switched according to which phase is faulted? To what phases is the receiving equipment coupled, or is it automatically switched according to which phase is faulted? These questions are important because tests made by BPA have shown that the coupling arrangements most desirable from a standpoint of cost and simplicity will not always give satisfactory results.

We understand that the present equipment installed to locate transient faults pulses the line continuously at all times. Is this to be the standard arrangement? If so, what considerations favor the use of this method in preference to automatic starting of the equipment at the time of the fault?

Some pulsing methods would apparently require permanent assignment of a portion of the carrier spectrum. This would be an important factor in any location where the carrier spectrum is crowded. How broad a frequency band will have to be reserved to avoid interference?

I believe that the feasibility of building a practical and highly accurate locator for transient faults is assured on the basis of results reported. I do not know whether the device which will ultimately be most widely used will take the form proposed by the authors, or one of those Stringfield and I have proposed in our companion paper,[3] or some still different form. Regardless of the outcome, and we are quite open-minded, the work of Leslie and Kidd is an important contribution to solution of the over-all problem.

We have greatly appreciated the opportunity of exchanging information with Leslie and Kidd while their work and ours has been in progress.

REFERENCES

1. FAULT LOCATION ON HIGH VOLTAGE OVERHEAD TRANSMISSION LINES, Wilfred G. Hoyle. *Paper Number 217*, International Conference on Large High Voltage Electric Systems (Paris, France) 1948.

2. THE LOCALIZATION OF FAULTS ON OVERHEAD LINES BY MEANS OF IMPULSE WAVES, S. Margoulies, P. Fourmarier. *Paper Number 307*, International Conference on Large High Voltage Electric Systems, (Paris, France) 1948.

3. A TRANSMISSION LINE FAULT LOCATOR USING FAULT-GENERATED SURGES, R. F. Stevens, T. W. Stringfield. AIEE TRANSACTIONS, volume 67, 1948, pages 1168–79.

J. R. Leslie and K. H. Kidd: Stevens has asked for details of the transient installation as now operating. Leaside transformer station is the terminus of four 220-kv circuits ranging from 54 to 303 miles in length, and normally paralleled on the 220-kv bus. The waveburst Linascope is connected to one phase of this system, through a tuned coupling capacitor. Three of the four circuits are provided with relays which initiate the Linascope in the event of a circuit fault. A photograph of the Linascope trace of the faulted circuit is taken by momentarily switching on the Linascope pulse transmitter and brightening the cathode-ray trace. After a one-second interval, a second record is taken which will normally correspond to the circuit when restored to normal.

A transient fault has not occurred on these circuits since the Linascope was installed in July 1948.

The unit may be used visually in case of a sustained fault by connecting the output to the faulted circuit. Routine instructions have been issued to the operators to cover such an emergency.

The relative merits of a d-c or waveburst pulse require further study. Tests with the two pulses definitely indicate that the latter gives considerably greater sensitivity when connected through a coupling capacitor. However this factor may be offset by the greater ease with which a d-c pulse can be generated.

The use of high-speed switching to select the faulted circuit, as suggested by Spaulding, will provide a suitable method of monitoring several circuits when they are not normally in parallel.

It is felt that the use of a repetitive surge offers one fundamental advantage over a single pulse in that a reflection is more likely to occur at the moment of fault current maximum.

Noakes, Evans, and Taylor remark that photographic recording introduces an objectionable delay, whereas the authors feel that most transient faults need not be located immediately. The use of an extremely long persistence screen or of magnetic recording which would allow immediate playback are both interesting alternatives.

A Transmission Line Fault Locator Using Fault-Generated Surges

R. F. STEVENS
MEMBER AIEE

T. W. STRINGFIELD
MEMBER AIEE

THE characteristics of an ideal fault locating device are listed. These are contrasted with those of the methods available in the past. Some method based on measurement of the time required for a wave to travel between the fault and a known point is believed to offer the best chance of fulfilling the ideal requirements. Three systems using this general approach are listed, namely,

1. The "pulse radar" method.
2. The "modulated frequency" method.
3. The "fault-generated surge" method.

The paper describes the development, by the Bonneville Power Administration (BPA) of two different types of fault locator, both of which make use of fault-generated surges. These are called for convenience the type *A* and type *B* fault locators, respectively.

A type *A* fault locator has been in successful operation on the system of the BPA for about a year. This device is generally applicable to high-voltage lines, and is far more accurate than other methods used previously. However, it requires use of photographic film, and, in its present form, considerable experience is necessary for quick and accurate interpretation of results.

The type *B* system is less generally applicable; but, where it can be used, will give an accurate indication or chart record of the distance to the fault in miles, without resorting to photography. This device approaches quite closely to the ideal as defined.

Both the type *A* and type *B* systems are automatic, are designed for coupling to live lines, and will complete their measurements before the fault arc is extinguished. Therefore, unlike most other systems and devices, they will locate those faults (constituting about nine out of ten of all faults) which are not sustained, as well as the smaller number of faults which are.

The possibility is suggested that one of these systems may someday be used as a basis for a fundamentally different and extremely fast system of power line protective relaying.

The problem of locating transmission line faults is as old as the power industry itself. As transmission distances have grown longer, and as more lines have been built through isolated mountain and desert areas, the importance of the problem has grown. Meanwhile, the available fault locating methods have remained very inadequate indeed.

The engineers of the Bonneville Power Administration have for some time been seeking a better solution. One of the steps was to define the requirements for a theoretical ideal fault locator. An attempt was made to build a practical device to meet these requirements, or to approach them as closely as possible.

Requirements of the Ideal Fault Locator

The requirements of the ideal device were established as follows:

1. It should complete its measurement before the fault arc is extinguished. (In this way it will locate faults which are not re-established when the line is re-energized but which may, nevertheless, have caused damage.)

2. It should be able to locate a fault to the nearest tower or span. (Accuracy of the order of $\pm 1/10$ mile.)

3. It should be designed for installation in an accessible spot (preferably an attended substation at a line terminal).

4. It should not require slow and cumbersome work, such as developing photographic film, before answers are available.

5. Results should be in a form which can be interpreted readily by any station operator or line foreman.

6. Equipment should operate with safety to personnel and service.

7. Equipment should be simple, rugged, and relatively inexpensive.

Limitations of Previous Methods

The contrast between these requirements and the capabilities of past methods is striking. Patrol of an entire line from the ground is extremely slow, and does not always locate the point of fault. Patrol by air (slow plane or helicopter) is faster but limited by weather and terrain, and by availability of aircraft at time of fault.

Sectionalizing switches, by isolating a sustained fault in a line section, simplify the problem but do not solve it, since the faulted section must still be patrolled. Moreover, unless the switches are attended by operators, or carrier-supervisory-controlled (expensive in either case), valuable time will be lost before they can be operated. This method is of no help where the fault is not sustained.

Another aid, the commercially manufactured annunciator ammeter, is useful, but it has an error of 15 or 20 miles or more under unfavorable conditions. Precision is limited because the current is measured in steps and because of variations in tower footing resistances. A further disadvantage is that system connections at the time of fault must be considered. (To overcome the first of these limitations, BPA has developed a modified ammeter with a continuous scale

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R. F. Stevens is chief, design section, and T. W. Stringfield is communication engineer, Bonneville Power Administration, Portland, Oreg.

The authors wish to express their appreciation to L. R. Spaulding and W. V. Behrens in this acknowledgment of their suggestions and work in the design, construction, and test of the equipment; also, to M. G. Staton and his associates at the Radio Corporation of America for suggestions regarding the directional coupling device.

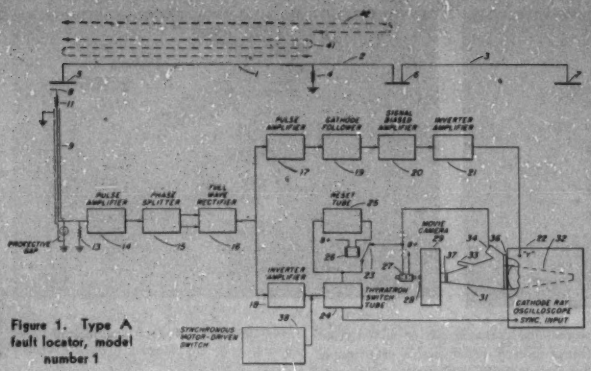


Figure 1. Type A fault locator, model number 1

which is actuated by a charge stored in a capacitor by the fault current and proportional to its magnitude.)

Another device,[1] developed by BPA, is the use of an iron core magnetized by the fault current. After the fault, the degree of magnetization is measured. Other devices, including electronic devices, have been developed commercially to aid in locating faulty insulators while patrolling the line. Each of these methods, however, is lacking in one or more of the important characteristics desired.

Traveling Wave Methods

All experience to date seems to indicate that the best hope for a better fault locator lies in the use of some method for measuring the time for a wave or pulse to travel between the fault and a fixed reference point. This has also been suggested in a paper[10] by K. Berger.

The systems which use this principle can be divided into three main categories namely:

1. *The Pulse Radar Method.* This uses a pulse of high-frequency oscillations (or a d-c surge) artificially generated and impressed on one end of the line. It travels to the fault, from which it is reflected back to the source. One half the time of round-trip travel, multiplied by the propagation speed (for practical purposes, the speed of light) is equal to the distance to the fault.

2. *The Frequency Modulated Method.* Here a high-frequency voltage is impressed on one end of the line, from which it travels to the fault and is reflected back to the source. The frequency of the impressed voltage, however, is varied with time at a linear rate. The reflected signal, arriving at the source, therefore, will have a different frequency than the frequency being transmitted at that same instant. The difference in these frequencies is measured and is proportional to the distance to the fault.

3. *The Fault-Generated Surge Method.* This method uses the surge generated by the collapse of voltage at the fault itself. Two systems have been developed for the application of this method and, for convenience, are designated as the type *A* fault locator and the type *B* fault locator.

A. In the type *A* system, the surge from the fault travels to a station at one end of the line, where it starts a timing device. It is reflected from the station bus, back down the line to the fault, where it is reflected, and it again travels to the station. The time interval between the first and second arrival of the surge at the station is measured by the timing device, and is proportional to the distance to the fault.

B. The type *B* locator system makes use of both of the surges which originate at the fault, and which travel down the line in opposite directions. One of these arrives at what may be called the "near end" of the line, and triggers a timing device. The other travels to the "remote end." On arrival there, it causes a broadband radio transmitter to send out a timing pulse, which is received at the near end of the line and used to stop the timing device. The elapsed time interval (less a constant correction for the difference in length between the path through space and the path along the line) is a measure of the distance to the fault.

The Pulse Radar and Frequency Modulated Methods

Most of the work done by other investigators[2–9] has been based on either pulse radar or frequency-modulation (FM) methods, and some success has been reported.

However, in order to meet the exacting requirements for the ideal fault locator previously set forth, it was decided to concentrate on other approaches to the problem. This was because of the following difficulties anticipated in the application of the pulse radar or FM methods:

1. High cost of equipment for generating, and impressing on the line, through a suitable broadband coupling, the artificial pulse.

2. Difficulty of obtaining the desired precision with a carrier of frequency sufficiently low for transmission along a power line.

3. Obscuring of the relatively weak artificial pulses by the far stronger pulses originating at the fault itself and by successive reflections of such pulses.

These objections are less valid if use is limited to location of sustained short circuits, or open circuits, on dead lines. Additional study may also overcome some objections to use on live lines. For instance, it might be practical to delay sending out the artificial pulse long enough for the initial transients from the fault to be greatly attenuated, yet not long enough for the arc to be extinguished.

The Type A Fault Locator System

Concern over the effect of fault-generated surges in masking signals under the pulse radar system led to the idea of using these very surges themselves as a means of measurement. This resulted in development of the type *A* system shown diagrammatically in Figure 1.

In this diagram (1) is a transmission line connecting two substations (5) and (6). Let us assume that a phase-to-ground short circuit occurs on this transmission line, represented as a resistance to ground (4). The line voltage at this point, collapsing suddenly from a value expressed in tens of kilovolts, to a lower value, causes traveling waves (41) and (42) to move along the line in opposite directions from the fault. When wave (41) arrives at bus (5), it is picked up by capacity coupling (8), which is of the order of 15 micromicrofarads. Resistor (11) in conjunction with capacitor (8) will be recognized as a differentiating cir-

Figure 2. Coupling device

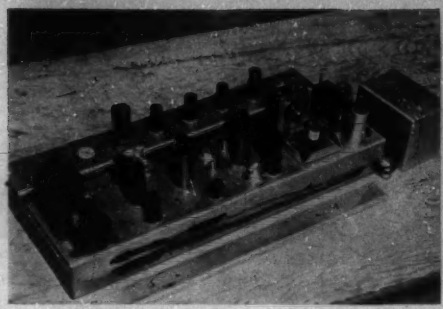


Figure 3. Control amplifier

Figure 4. Type A fault locator

cuit or high-pass filter, passing only the steep-wave front of a traveling wave, and resulting in a sharp pulse of short duration being fed into pulse amplifier (14). Resistor (13) is used to terminate coaxial line (9) in its surge impedance. Resistor (11) is very much larger than resistor (13) and the two together step the voltage down to a low value. The traveling wave may be either negative or positive in polarity, depending on the instantaneous line voltage at the time the fault occurs. However, it is desired in this case to display this pulse on the indicator screen in an upward direction with respect to the X-axis, regardless of the surge polarity. This avoids confusion with range markers. Therefore, the output of the pulse amplifier (14) is fed into a conventional phase splitter (15), the output of which is fed in turn into a conventional full-wave rectifier (16) connected for negative output. Hence, regardless of polarity, the polarity of the pulse emerging from the rectifier tube is always the same.

The pulse output of the rectifier is fed into two separate paths. The first path provides the input of the Y-axis plates of a cathode ray oscilloscope. The second path provides the trigger which initiates the horizontal sweep. In the first path, the pulse is fed into a conventional pulse amplifier (17), thence into a cathode follower (19) which overdrives a remote cut-off type pentode (20) and sets the grid leak bias in accordance with the magnitude of the first pulse. The gain thus varies inversely with the magnitude of the first pulse and is set for the duration of the sweep. The output of the signal biased amplifier is then inverted and amplified in inverter amplifier (21). From here it is led to the Y-axis input of cathode ray tube (25) to furnish upward deflection of the scope trace. In the second path, the negative pulse output from tube (16) is fed into inverter amplifier (18) which triggers the thyratron switch tube (24). The output from the cathode resistor of (24) triggers the oscilloscope which then produces a single high intensity sweep.

Meanwhile, the surge (41) which started this train of events is partially reflected from the substation bus. It travels back toward the short circuit, where it is again partially reflected back to bus (5). On this second arrival, a pulse is again fed to the Y-axis deflection plates, producing a second pip on the trace. This second pip is displaced from the first pip a horizontal distance proportional to the round trip travel time of the surge between bus (5) and the short circuit (4).

Wave (42) in the meanwhile has traveled to the far end of the line where it is partially reflected at bus (6) and travels back to substation bus (5) causing a third pip which is displaced from the first pip a horizontal distance proportional to the round trip travel time between the fault and bus (6). Wave (42) returns through the fault point by induction into the unfaulted phase conductors and through the fault itself since the tower footing resistance is usually appreciable. Since the identity of the pips produced by waves (41) and (42) is ambiguous, the fault may be at either of two locations depending upon the interpretation. However, experience to date (though very limited) indicates that the wave (42) reflected, from the far end of the line, usually produces a steeper pip and of somewhat greater magnitude than that for the wave reflected from the fault and this may be of considerable use in proper identification. Annunciating ammeters also furnish supplementary information as to in which half of the line section the

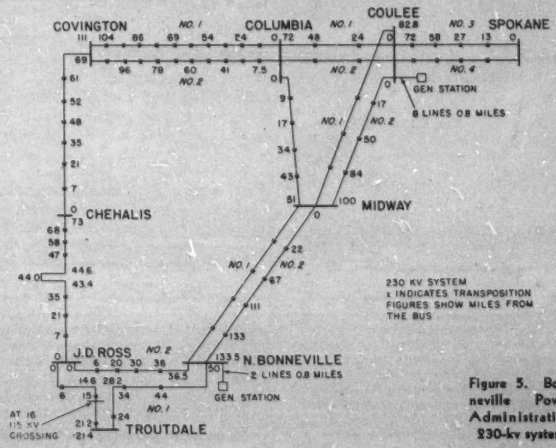


Figure 5. Bonneville Power Administration 230-kv system

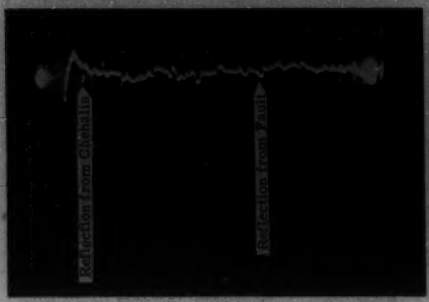


Figure 6. Test on J. D. Ross–Chehalis 220-kv line

Single phase-to-ground arcing fault, 64 miles from J. D. Ross. Fault locator at J. D. Ross. Line open at Chehalis

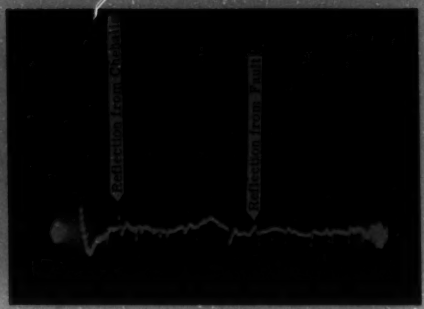


Figure 7. Test on J. D. Ross–Chehalis 220-kv line

Single phase-to-ground arcing fault, 58 miles from J. D. Ross. Fault locator at J. D. Ross. Line open at Chehalis

fault occurred. The foregoing does not apply to phase-to-phase faults where, because of the low fault resistance, wave (42) for practical purposes does not penetrate through the fault and thus does not produce a pip.

The oscilloscope is provided also with a calibrated range marker circuit which produces downward pips at intervals of ten line miles, enabling the fault distance to be read directly with reasonable accuracy. Since the trace is nonrepetitive, and persistence of the screen is limited, it is necessary to photograph a single trace. This is done by means of a 16 millimeter movie camera (29) which has its shutter always open and which views the cathode ray tube face through light shield (31). Thyratron switch tube (24), when fired, operates relay (27), the armature of which strikes a button (28) on camera (29) causing a new frame to rack up. Thyratron switch tube is reset by reset tube (25) which operates relay (26), momentarily opening the contacts (23), breaking the plate current to (24). Fogging of the film due to heater glow in the cathode-ray tube is reduced by a suitable light filter (37), by tilting the camera and also by racking up a new frame once an hour by means of a trigger produced by a synchronous motor-driven switch (38).

The used film is automatically stored in a light-proof container attached to the camera and readily removable. This container is a daylight developing tank, means being provided to introduce developer and fixer. After developing and fixing, the film is dried and viewed through an enlarging viewer in order to read the fault distance.

Recording of the time of the various photographs is done by means of an argon lamp (34) which is flashed by the voltage surge across the operating coil or relay (27). This momentarily illuminates the clock face (36) which is in view of the camera and records the time on the film before the next frame is racked up.

Operation of the equipment may be checked by means of a test unit which simulates a short circuit by furnishing two successive pulses, the magnitude and spacing of which are adjustable. The sweep trace may be viewed through peep (33) in the light shield.

Description of Equipment

Figures 2, 3, and 4 show the various units of the fault locator. These include a voltage coupling device with the necessary coaxial cable, a control amplifier unit, an indicator, and a camera. The coupling device shown in Figure 2 is inexpensive and simple. It is merely a piece of one-inch pipe about four feet long supported in a horizontal position parallel to and beneath the bus. Incorporated into the support is an insulating tube inside of which is the resistor of the differentiating circuit. In the experimental equipment, the support itself was a portable pipe tripod with a pipe extension and micarta tube. In a permanent design, one of the regular bus support pedestals might be used. Coaxial cable is used to connect the coupling unit to the control amplifier unit (Figure 3) normally mounted in the substation control house.

A standard test oscilloscope manufactured commercially was used as an indicator in the first model. Essentially, the indicator unit must provide a driven sweep, a high intensity trace, a wide-band video amplifier and a range marker circuit.

Minor modifications were made to the oscilloscope circuits, and, in addition, the control amplifier was provided, to receive the input signal and shape and amplify it to the proper level, and to control and synchronize the functioning of the equipment. If there should be occasion to produce this equipment in quantity, the indicator and control amplifier would doubtless be combined in one case, and some of the unneeded features of the present oscilloscope would be omitted.

Operating Experience

In Figure 5 is shown the BPA 220-kv system on which the first fault locator was tested and is presently installed. In Figures 6, 7, and 8 are shown oscillograms for staged single phase-to-ground short circuit tests on the J. D. Ross–Chehalis 220-kv line. The line was connected to the system at J. D. Ross only, being open circuited at Chehalis during the tests. The short circuit was applied by pulling a fine wire, grounded at one end to the tower, into the line conductor. The fault tests for Figures 6 and 7 were the first short-circuit tests made using the type *A* fault locator and the distances to the faults were unknown to the operators. The distance to the fault was determined, within an error of one mile, from the

Figure 8. Test on J. D. Ross–Chehalis 220-kv line

Single phase-to-ground fault 30 miles from J. D. Ross. Line open at Chehalis

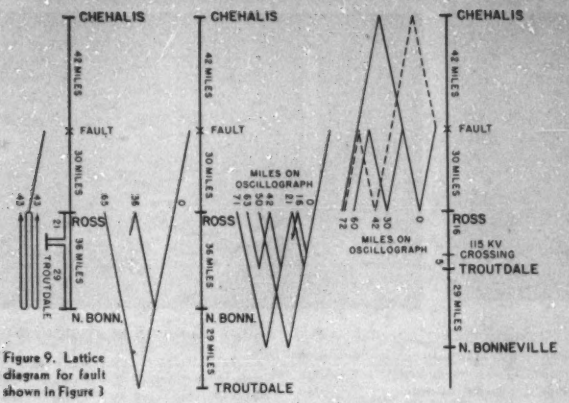


Figure 9. Lattice diagram for fault shown in Figure 3

oscillographs in each case. During these tests, the signal was fed directly into the oscilloscope Y-axis so that positive and negative surges could be distinguished. In Figure 8, the fault distance was known beforehand, the purpose of the test being to check the operation of the signal biased amplifier which sets the gain for the sweep duration. Figure 9 is a lattice diagram,[11] for the fault shown in Figure 8, and together with Figure 5 is a means for showing the time of arrival of the waves producing the pips.

Figures 10, 11, 12, and 13 are oscillograms for line faults obtained under service conditions with the type *A* fault locator installed at Spokane, Wash. Unfortunately, from the standpoint of gaining data and experience, lightning trouble in the Spokane area was unusually light during this season. Practically all of the strokes occurred on the line sections beyond Grand Coulee which is considered the range of the type *A* fault locator. However, several of these faults were successfully located.

Figure 10 and Figure 11 are oscillograms for faults occurring 24.1 miles from Columbia and 31.5 miles from Coulee, respectively, on the Coulee–Columbia line, and were successfully located even though they occurred in the next line section and at a distance beyond the normal range of the instrument. This is possible because of the length of line (82 miles) between Spokane and Grand Coulee in effect cancels out for the initial wave and the waves reflected from the fault and Columbia, since these are delayed by the time required to traverse the Grand Coulee–Spokane line section. On this basis, the oscillograms were interpreted as if the fault locator were installed at Grand Coulee instead of Spokane.

Figure 12 is an oscillogram obtained for a phase-to-phase fault on the Spokane–Coulee line and caused by arc-over when the conductors whipped on ice unloading. This occurred at 72 miles from Spokane, according to the oscillogram, but no damage was found on a quick inspection of the line. The icing conditions on the line were most severe for several miles at this distance, and also, the annunciator ammeters indicated the fault to be in that neighborhood. A total of five such faults occurred within a half-hour period at approximately the same distance and were similarly recorded. A careful scrutiny of the conductors for burns has not been made to date.

Figure 13 is an oscillogram obtained for a lightning stroke on the Columbia–Midway line. Due to the fact that the location of the fault locator was at Spokane, the interpretation of this oscillogram is difficult because of the Coulee–Columbia–Midway loop. On the basis that the initial wave could travel directly from the fault to Spokane over two paths and that the first to arrive triggered the sweep and the second to arrive caused the oscillations beginning at 11 miles, it was deduced that the fault occurred at 28 miles from Columbia. The remaining pulses could then be accounted for on the basis of reflections using a lattice diagram (not shown). The annunciator ammeter indicated the fault at 0 to 26 miles from Columbia. A search for damage from this fault has not yet been made.

Improvements in Second Model

The purpose of the first model was to prove the feasibility of the basic method, and no attempt was made to achieve extreme refinement or precision. Methods by which this could be done will not be discussed in detail, as they will suggest themselves to any engineer familiar with radar techniques. Some of these are exemplified in the number 2 model which is being designed for monitoring the 167-mile 230-kv line being constructed from Grand Coulee to Snohomish, Wash.

Figure 14 is the block diagram for this model, and Figure 15 shows the type of signal presentation used.

By making the sweep circular, it is possible to increase its length by a ratio of

Figure 10 (left). Lightning stroke on Coulee – Columbia 220-kv line number 1

Fault 24.1 miles from Columbia. Fault locator at Spokane

Figure 11. Lightning stroke on Coulee–Columbia 220-kv line number 1

Fault 31.5 miles from Coulee. Fault locator at Spokane

Figure 12. Ice unloading. Phase-to-phase fault on Spokane-Coulee 220-kv line number 3

Fault 72 miles from Spokane (not found). Fault locator at Spokane

Figure 13. Lightning stroke on Columbia–Midway line

Fault 28 miles from Columbia (not found). Fault locator at Spokane

three to one. Range markers are not displayed on the same trace as the signal, but on a separate concentric trace of slightly smaller diameter. These factors, together, make it practical to provide range markers at intervals of one mile as compared to ten miles in the first model.

It is proposed to use a crystal controlled oscillator for range markers. A lower frequency (possibly supplied by count down from the range mark oscillator) will supply sine and cosine waves, to the X and Y axis plates respectively, to generate the circular sweep.

In this model, the signal pulses are displayed inward or outward from the trace, according to their actual polarity. This is an aid to identification of the signals. It is feasible because the range markers are displayed on a separate calibration trace. The phase splitter and rectifier, though removed from the main signal path to the oscilloscope, are retained to provide the positive pulse to set the signal-controlled gain.

Another proposed improvement, to be developed, is a directional coupler which will accept only incoming surges from the line to be monitored. Such a device, if successful, will eliminate most of the larger spurious reflections. Two methods have been suggested. One involves use of an additional electrostatic coupling to the line several hundred feet from the station. The other involves use of a shielded electromagnetic coupling in conjunction with the electrostatic coupling previously described. Outputs will be adjusted to the same level and combined in such a way that they will add for an incoming, and cancel for an outgoing surge.

Application of Type A Fault Locator

For a complete application to a large power system, it is intended that one type A device will be installed at each major substation, with an electrostatic coupling device positioned under the

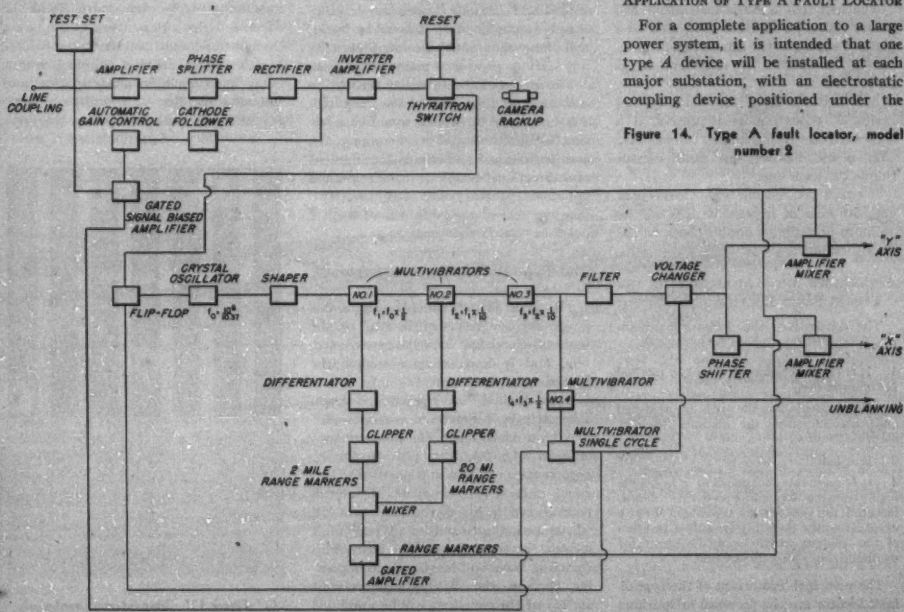


Figure 14. Type A fault locator, model number 2

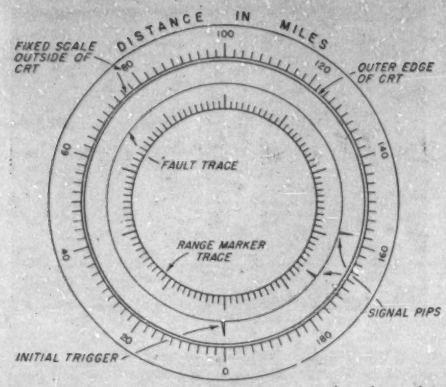


Figure 15. Cathode ray tube presentation for type A fault locator, model number 2

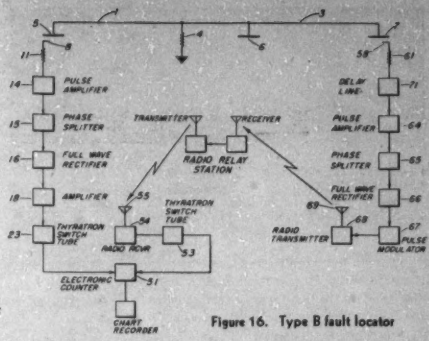


Figure 16. Type B fault locator

middle phase of the bus. In this position, the equipment will record a fault occurring on any phase, of any line radiating from the substation. While the equipment itself will not discriminate between lines or phases, this information is available from the regular indicating lamps and relay targets.

The fact that a single instrument will record faults on all lines connected to a given station has the advantage of minimizing the amount of equipment necessary.

An alternate arrangement may be found desirable, though more expensive, if a successful directional coupler can be built. This would involve one fault locator coupled to each line.

In either case, installation of locators at opposite ends of important lines will be useful in providing a double check on the fault location.

Advantages and Limitations of the Type A Fault Locator

The device has the following advantages:

1. It is far more accurate than any method in common use.

2. It operates before the arc is extinguished and thus measures the distance to nonsustained faults.

3. It is safe.

4. It is relatively inexpensive. The cost is highly variable depending on number manufactured and refinements provided. In any event, it would probably be only a fraction of the amount which would be spent for sectionalizing a long 230-kv line.

The principal limitations of the type *A* fault locator are the presence of spurious pulses, the requirement for taking photographs and developing film, which is comparatively slow and cumbersome, and the training required for correct interpretation of the records.

If it were possible to build into the equipment the necessary brains to discriminate with certainty between the true and spurious pulses, then it would be possible to operate an indicator or recorder from which one could read, directly, in miles, the distance to the fault. This would eliminate the need for developing photographic film; it would be faster, and the results could be interpreted by any station operator or maintenance man.

The cost could be decreased because no cathode-ray tube would be required. All that would be needed would be a few simple radio tubes and other components, and the indicator. Perhaps development of a directional coupler, or some method not now apparent, may later make this improvement of the type *A* fault locator possible.

The Type B Fault Locator System

The type *B* fault locator system was designed by engineers of the BPA on the basis of experience with the type *A* system, and is designed to overcome the objections to the latter.

The method of operation, shown schematically in Figure 16, is as follows:

A transmission line (1) connects substations (5), (6), and (7). Upon the occurrence of a fault at point (4), two equal surges will originate. One will travel down the line to the substation (5), where the indicator unit of a type *B* fault locator is coupled to the bus. This traveling wave will be sharpened in passing through the differentiating circuit (8, 11) of the coupling, will be amplified in the pulse amplifier (14), and will then go to the pulse splitter (15) and full-wave rectifier (16), all in the same manner as described for type *A* equipment. The output of the full-wave rectifier will always be a negative pulse. This will be amplified and inverted in the next stage (18). The output triggers a thyratron switch tube (23) which starts an electronic time interval counter (51). Meanwhile, the other surge is traveling down the line in the opposite direction. It eventually reaches substation (7) at the extreme right. Here there is coupled a "remote end unit" of the type *B* fault locator, which includes coupling device, delay line, amplifier, phase splitter, and full-wave rectifier. The full-wave rectifier, as before, puts out a pulse of negative

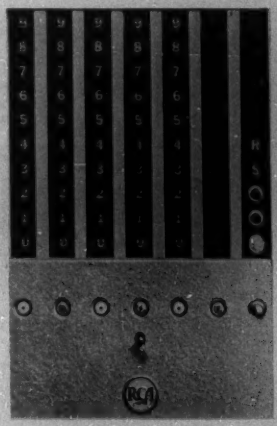


Figure 17. Time interval counter

May 10 1948	03:45:11 AM	013.7 Mi.
May 9 1948	11:09:53 PM	206.5 Mi.
May 9 1948	07:33:09 PM	206.5 Mi.
May 9 1948	12:31:45 AM	029.9 Mi.
May 8 1948	06:10:35 PM	189.6 Mi.
May 8 1948	06:08:35 PM	168.8 Mi.
May 8 1948	09:20:34 AM	98.9 Mi.
May 7 1948	09:59:17 PM	87.1 Mi.
May 5 1948	11:21:02 AM	125.2 Mi.

Figure 18. Proposed type of chart record for Type B fault locator

polarity regardless of the polarity of the original traveling wave. This output pulse is used to modulate a radio transmitter. This radio signal is transmitted either directly or via intermediate radio relay stations to the radio receiver (54) at the other end of the line. The output of this receiver triggers thyratron switch tube (53) which stops the action of the electronic counter (51). The delay line (71) may be a fixed or variable delay to zero the equipment for difference in path length of the transmission line and airline. It may also be installed in the receiver (54) output.

The electronic time interval counter (Figure 17 shows a standard commercially manufactured product) is a decade box consisting of six rows of lamps, ten lamps to a row, and is capable of recording elapsed time in microsecond units up to 999,999. This counter can readily be calibrated in terms of distance to the fault, to the nearest 0.1 mile, consideration being given to the constant propagation time between the radio transmitter and receiver.

The advantages of such an indication as compared to the development and interpretation of a photographic film are apparent. It is merely necessary to log the reading and reset the device ready for the next signal.

If a permanent record is desired, a method has been devised for providing this by adapting a commercially manufactured chart recorder for use with the time interval counter. One type wheel with digits 0 to 9 is provided in the chart recorder for each decade of the interval counter. Each such type wheel is notched ahead in synchronism with a 10-point 2-deck telephone-type selector switch or notching relay. The latter samples in turn each lamp circuit from 0 to 9 of the corresponding decade of the interval counter, or until an energized lamp circuit is found, whereupon the notching action stops. For instance, if lamp number 7 is energized, then the notching relay and the type wheel stop rotation when digit 7 on the latter is in position to print.

This sampling process starts upon arrival of the signal, and stops when the distance to the fault (in miles and tenths) is set up on the type wheels in agreement with the lamp indication. This is then automatically printed on the chart, together with the date and the time of day in hours, minutes, and seconds. Figure 18 shows the type of chart record to be used. The device then resets automatically.

Application of the Type B Fault Locator

Experience gained in the use of the type *A* locator, plus the general information already available from the electronics field, has made it possible to proceed with confidence on designs for the first type *B* fault locator installation (Figure 19).

This will provide monitoring of faults on the lines connecting the Snohomish, Covington, and Olympia substations of the BPA, and will be put in operation as soon as the microwave radio relay links between these stations are completed.

Figure 19 shows a type *B* remote end unit at Snohomish and one at Olympia, and a type *B* indicator unit at Covington. This would make it possible to locate faults occurring on the BPA line between Covington and South Renton, and on city of Seattle Lines between South Renton and Seattle.

When the radio relay communication system is completed south to J. D. Ross, the indicator unit will be moved from Covington to the latter station, from which point it will be able to locate faults anywhere in the 230-kv system between Ross and Snohomish, a total line distance of approximately 170 miles.

It will be seen that the type *B* fault locator equipment is not needed at each end of each line section to give full coverage. Instead, indicator and remote end unit can be placed several hundred miles apart and still monitor the circuits between regardless of the number of intermediate substations.

In addition, it appears that the identical equipment installed to monitor the direct circuits between Ross and Snohomish substations will, at the same time, monitor a total of 534 additional circuit miles of the transmission network, lying mainly in central Washington.

This feature which comes as an extra dividend is most welcome, as the incidence of lightning along these line sections is considerably higher than the general average for the region.

Sections of line accurately monitored by the single fault locator installation are shown in heavy lines in Figure 20. Line sections shown by a narrow line are monitored only to the extent of indicating that the fault is in that section. Furthermore, the points of zero mileage reference for the line sections in central Washington are not Snohomish as is the case for the direct lines between Snohomish and J. D. Ross. However, these zero points may be established readily by a study of Figure 20, remembering that the indicator unit at J. D. Ross is receptive to only one pulse from the line coupling and only one pulse from the radio receiver, and whichever arrives first starts the time interval counter and the second to arrive stops it. In order to facilitate identification of the fault location, charts will be provided for each line giving tower number versus mileage as indicated by the fault locator. The identity of the line section involved would be provided by knowledge of circuit breaker operation.

Experience with the type *A* fault locator leaves little doubt that surges traveling the longer distances from faults in central Washington will arrive with sufficient amplitude to operate the equipment. To what extent precision of measurement will suffer from flattening of the wave front as the surge travels the greater distance remains to be shown by actual test.

It is expected that surges from switching at intermediate stations will operate the equipment, providing valuable calibration points without the necessity of staging fault tests.

With the future extension of the microwave communication system, it will be possible to add additional fault locator equipment which will remove the areas of ambiguity and provide accurate monitoring of faults on all portions of the main transmission network.

Economies of the Type B Fault Locator

It should be said that the type *B* locator can only be applied economically under certain special conditions. Specifically, it requires a broadband radio link between the two ends of the line. Power line carrier might perhaps be used at considerable sacrifice in precision.

The cost of a radio link for fault location alone would generally be prohibitive. However, there are likely to be many places where such a link will be installed for other purposes. The BPA, for instance, had under way plans for a multiplex microwave radio link system with channels available for communication,

telemetering, facsimile, relaying, and supervisory control to all its major substations. This system was planned for these basic uses, without, originally, any regard to its possible use for fault location.

It is believed that in time many other power utilities will find it to their advantage to make similar applications of radio relay for communication, telemetering, and the like. Such action is likely to be hastened by overcrowding of the spectrum suitable for power line carrier, as well as cost advantages of radio for multichannel use. Such a system can be adapted at nominal cost to fault location without interference with its primary functions.

Where the utility does not wish to develop its own radio links, it may be well to explore the use of microwave links owned by others. Such links are being planned in increasing numbers by telephone and telegraph companies, radio manufacturers, and broadcasters. Fault locator signals are transmitted for less than ten microseconds. Compared to this, 2,000 microseconds are required for transmittal of one cycle of a 500-cycle audio tone, and about 30,000 microseconds are used for transmittal of one television frame. Obviously, a ten microsecond interruption to either sound or television transmission would not be perceptible.

Advantages of Type B Locator

It is believed that the advantages of this equipment are apparent.

1. Cost, exclusive of the radio link, is small. The units at each end require only inexpensive radio tubes and components. No camera or cathode-ray tube is required.
2. Readings are instantaneous.
3. Readings can easily be interpreted by any operator or maintenance man. No special skill or training is required.
4. No delay in developing of film is required.
5. Devices of the type described can be installed at points several hundred miles apart and monitor the lines in between, regardless of the number of intermediate stations. This results in decreased cost, and a chance to put the indicator at a central operating point.
6. This type of equipment is not subject to ambiguities because of multiple reflections. The direct surge will always arrive first, will trigger the equipment, and any subsequent signals will be rejected. Sufficient amplification, therefore, can be used to insure positive operation of an indicator or recorder.

General Factors Affecting Operation, Attenuation and Reflection

All traveling wave methods require that attenuation and distortion be sufficiently small to permit accurate measurement of time of travel.

In addition, the pulse-radar frequency-modulated and type *A* locator systems require that the coefficient of reflection at the fault be high enough to give a useable reflected signal. In particular, it should be appreciably greater than those from transpositions along the line. This is not a requirement for the type *B* locator, which does not make use of any reflections.

In addition to both of the foregoing requirements, the type *A* fault locator also requires a high coefficient of reflection at the bus itself. Such a condition almost invariably exists. None of the other systems makes use of reflections from the station bus.

Attenuation

Based on well-known theoretical considerations,[7] a traveling wave with a nearly rectangular wave front on a typical 220-kv line would be attenuated to approximately 60 per cent of its initial value after traveling 100 miles on the assumption that there are no corona losses, no leakage conductance, infinite earth conductivity, and that the line resistance is corrected to a frequency of one megacycle. This indicates that a traveling wave generated by an arc-over may be useable for precise range measurement even after traveling several hundred miles or more. The crest voltage of a wave generated by arc-over would be less than normal line voltage and therefore corona and distortion caused by it would be absent. In the case of a surge caused by a combined lightning stroke and power arc-over, only that portion of the leading edge of the traveling wave below the corona voltage would be used for measurement. However, the unpredictables involved in a lightning discharge would enter and recourse must be had to the accumulation of service data by actual installations to determine the practicability in this case.

Reflection from Line to Ground Fault

The surge impedance to ground at an arcing fault to tower is made up of the arc surge impedance, tower surge imped-

Figure 19. Proposed initial installation, type B fault locator

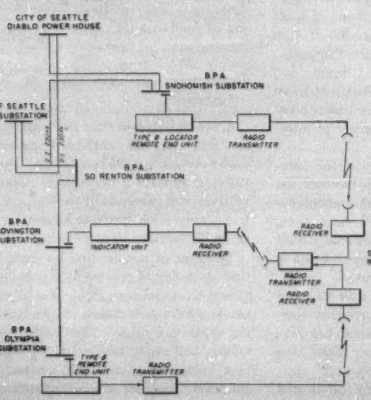
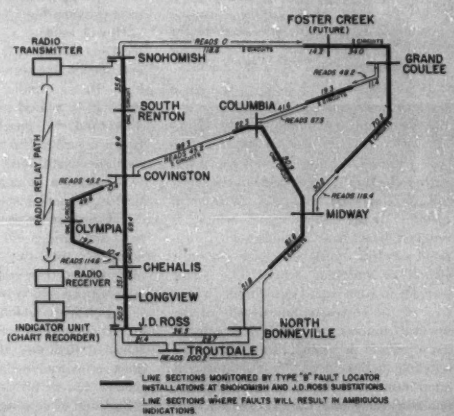


Figure 20. Use of single type B fault locator installation to monitor complex transmission network



ance and tower footing surge impedance to ground, including counterpoises and driven rods, if used. The arc impedance may vary from less than 10 ohms to several thousand ohms, decreasing with current magnitude and increasing with arc length.[8] Consideration of this may be of importance in a pulse-radar type of fault locator which employs a pulse generated at the far end of the line, where the application of the test pulse is delayed for the purpose of allowing surges produced by the fault to attenuate. If the test pulse arrives at the fault when the 60-cycle current is near or at zero, an inadequate reflection may be obtained for measuring purposes. On the other hand, where the fault surge itself is used, as in the type *A* fault locator herein described, it seems reasonable to assume that the arc current will be sufficiently established and the arc resistance low to provide a high-reflection coefficient.

The tower surge impedance is assumed to be of the order of 75 to 150 ohms.[9] This, together with the tower footing resistance, will influence the slope of the front of the initial surge and all surges reflected from the fault, to some degree. However, because of the short physical length of the tower, it seems reasonable to assume that the successive reflections between top and bottom of the tower will probably decay to a low value within one microsecond. It is felt that the effect of tower surge impedance can, for the time being, at least, be neglected. Referring to several of many investigations on lightning reported in the literature,[10–13] these papers give data showing that the surge impedance of tower footings varies with current, and that the ratio of surge impedance to meggered resistance varies from unity to as little as 0.005. For the purpose of estimating the reflection coefficient for a ground fault at a tower, it is safest to assume that the tower footing surge impedance is equal to the meggered tower footing resistance.

Much has been published with regard to the nature of traveling waves caused by lightning on transmission lines, including oscillographic studies, and it is impracticable to give a complete bibliography. Referring to references[14,15] it may be seen that the wave fronts of such traveling waves would, in general, allow their use for fault locating purposes.

Reflection from Phase-to-Phase Fault

With phase-to-phase faults, of which five were recorded during system disturbances, it appears that the fault resistance is very low and the fault has a reflection coefficient practically equal to unity. Figure 13 is the oscillogram for such a fault. It will be noted from this oscillogram that there is no evidence of a reflection from the Grand Coulee bus on the far side of the fault. A defect in the cathode-ray oscilloscope circuit caused the first range marker to occur very nearly at the beginning of the sweep for this oscillogram.

Reflections from Station Bus

A fundamental requirement for the successful application of the type *A* fault locator is that the fault and the substation bus at which the fault locator is installed have a sufficiently high-reflection coefficient for a steep front wave. This condition would be fulfilled if the surge impedance at the fault and the substation bus is much lower than the surge impedance of the line. Furthermore, the reflection coefficients for discontinuities in the line such as transpositions, river crossings and line crossings should be small compared to the aforementioned reflection coefficients. The results of approximately ten tests with single phase-to-ground arcing faults at various tower locations on the J. D. Ross–Chehalis line indicate that spurious reflections due to line transpositions and river crossings are negligible. However, the surge impedance to ground at the faults was considerable as evidenced by the fact that the surge reflected from the far end of the line was transmitted through the fault (see Figures 7, 8, and 9). As a result, the reflection from the fault itself was not as great as hoped for. Substation busses have a high reflection coefficient due to the capacitance to ground of the bus itself and the associated equipment such as circuit breakers and transformers. If several or more lines terminate at the bus, the reflection coefficient is influenced still further. For a discussion and mathematical treatment of the theoretical behavior of traveling waves on transmission systems, the reader is referred to two papers by L. V. Bewley.[17,18]

False Operations

An unknown factor, at the start, was the number of false operations that could be expected from extraneous surges originating from remote portions of the Bonneville and interconnected systems due to switching and induced voltages from lightning. For this reason, a 16 millimeter movie camera with a 100-foot film capacity was chosen so that the number of such operations could be monitored. It was found that, while switching surges originating as far as 300 miles distant were identified as causing automatic operation of the equipment, the frequency of such operations was not excessive, for this type of application.

False operations do, however, constitute a major problem to be overcome in any possible future application of these methods to power line relaying, as discussed later.

Precision to be Expected

The precision of measurement depends primarily on the steepness of wave front if it can be assumed that the speed of propagation along the line remains constant. The electronic equipment itself is inherently capable of allowing far greater precision of measurement than the desired ultimate which is identification of the tower involved. If it is assumed that the surge travels with the speed of light, then a round-trip mile would correspond to 10.74 microseconds. In order to read distances to a precision of ±1 mile, the pulse derived from the leading edge of the traveling wave should have a rise time of approximately this order of magnitude. Tests to date indicate that this order of precision is easily accomplished and encourage efforts to extend the precision to ±0.1 mile to allow identification of the tower at or nearest the fault.

Errors due to variation in the speed of propagation will probably be encountered. Berger[19] reports measured values of 291 miles per microsecond, ±3 per cent as compared with 300 miles per microsecond in vacuum. The effects of ice and variation in earth resistance have still to be determined.

Coupling

Either voltage coupling or current coupling to the line may be used. In either case, the circuit constants of the coupling circuit are selected to provide a high pass filter so that only a small portion of the leading edge of the traveling wave is passed. A pulse of, say, approximately two to five microseconds' duration is thus obtained, the leading edge of which is used for measuring purposes. The 60-cycle component is, of course, almost completely rejected by this type of coupling. Preliminary tests have indicated that existing coupling equipment such as capacitor-type bushings with voltage taps, and even iron core current transformers may be successfully used.

Interpretation

With the type *A* fault locator, it has been found that the interpretation of a

recorded oscillogram is aided by comparison with a test oscillogram obtained by operating the line circuit breakers. On single phase-to-ground faults, the initial surge reflected from the bus at the far end of the line is usually distinguishable because of its greater magnitude and steep front (based on present knowledge) and its position along the base line is a measure of the distance between the fault and the far end of the line. The reflection from the fault itself may then be found at a distance corresponding to the distance between the fault and the substation at which the fault locator is installed. Also, if the first mentioned pulse is not readily distinguishable, then the oscillogram is inspected for two pulses the sum of whose base line distances equals the distance of the line section between the two substations. Approximate distance to the fault as obtained from annunciating ammeters is a further aid in establishing identity.

Relaying

Use of one of the fault-generated surge methods (especially the more positive type *B*) has been suggested for power line relaying.

To date, this has been explored by the writers in a preliminary way only. Many obstacles are apparent. Nevertheless, the method is the fastest conceivable way of determining where a line fault exists. As such, its possible application to line relaying constitutes an interesting challenge.

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Discussion

J. R. Leslie and K. H. Kidd (The Hydro-Electric Power Commission of Ontario, Toronto, Ontario, Canada): The pulse method of locating faults on transmission lines is attracting wide interest, as evidenced by a number of papers presented by various workers. Stevens and Stringfield are to be complimented for undertaking a fresh approach to the problem, that of using the surge generated by the fault instead of applying a pulse from an external source.

The research division of the Hydro-Electric Power Commission of Ontario is engaged in the development of the Linascope, a pulse-type fault locator for locating transient as well as sustained faults, as described in a companion paper.[1]

The writers feel that the Linascope has a number of advantages over the fault generated surge (FGS) method. These are outlined in the following paragraphs.

1. The FGS method is subject to limitations regarding its application to sustained faults. If, for any reason, it misses the initial fault surge, it is of no further use in locating the fault. For instance, it cannot be carried to the scene of the trouble and there put to use.

2. The type *A* FGS method gives a record which it is very difficult to interpret. The record is essentially the same as would appear on a Linascope connected at the fault and looking along the line in both directions.

3. The type *A* suffers the difficulties of single transient photography. With the Linascope, a conventional 1/25 second exposure is used.

4. In the type *A* method, the significant fault pulse undergoes two reflections, one from the station bus and one from the fault itself. The Linascope has only the latter reflection. In addition, for similar conditions, the type *A* pulse travels 50 per cent further than the Linascope pulse.

5. The pulse generated by the fault is likely to be highly variable in magnitude and steepness. The Linascope pulse applied to the line is constant and not subject to this variation.

6. The FGS method has an advantage in that it makes use of a greater pulse magnitude than does the Linascope. However, the present Linascope pulse amplitude of several hundred volts is adequate to show up transpositions, over more than 100 miles of line, which is the limiting sensitivity for either method. Furthermore, it will be quite feasible to increase the Linascope pulse to 10,000 volts or so (only turned on when fault occurs), if necessary.

7. The type *B* locator certainly offers the ideal, simplified form of presentation, but the cost will be prohibitive except in special cases. Care will be needed to prevent incorrect operation from spurious impulses.

8. The Linascope operation can be checked at any time on the live line.

The criticisms of the external pulse method offered by Stevens and Stringfield are not justified. The cost is not out of line with other proposed methods. A major item is the high-voltage coupling capacitor which can be shared with carrier equipment, if desired.

A precision of 1/2 of one per cent of full scale on 25 to 300 mile ranges has been obtained with existing equipment and this is considered to be adequate. It is hoped that interference from the fault generated surge will be avoided by providing a time delay of about ten milliseconds before taking the record. It has been stated that the Linascope pulse may reach the fault at a current

zero and hence not "see" the fault. However, since the Linascope pulse is repetitive (about 200 per second), several pulses will certainly arrive while the power frequency fault current is high.

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Richard F. Stevens: The following is an answer to the various questions raised by Leslie and Kidd.

1. It is true that the fault generated surge method cannot locate a transient fault if it misses the initial surge. However, any system which attempts to locate a transient fault will fail unless its action is completed within the fraction of a second before the arc is extinguished. If equipment is carefully designed and maintained, a failure coincident with the transmission line fault seems quite remote. In the case of a sustained fault, another chance for a measurement exists when the circuit breaker is reclosed.

2. We agree that some of the oscillographs published with our paper are difficult to interpret. However, recent tests indicate very considerable improvement can be obtained by changes in coupling methods. Changes proposed for the model 2 (separate range-marker trace, use of longer circular sweep, and display of positive and negative pulses on opposite sides of the base line) will also aid interpretation.

3. The photography of a single transient has not proved particularly difficult. Remarkably good results have been obtained with the f/1.9 lens of our standard 16 millimeter motion picture camera. However, we feel that somewhat better results could be obtained with a faster and more expensive lens. Repetitive traces with a 1/25 second exposure as proposed for the automatic Linascope would no doubt allow the use of a more inexpensive lens. Our main objection to the type A has not been the difficulty of photographing of a single transient, but the fact that it is necessary to wait for the development and interpretation of the film.

4. The fact that the significant fault pulse undergoes two reflections is an apparent disadvantage of the type *A* fault generated surge system. However, the coefficient of reflection at a substation bus is found to be quite high. Our main difficulty is getting a sufficient reflection from the fault arc itself. If the reflection from the substation bus at the far end of the line, instead of the second reflection from the fault, is used, only one reflection of the pulse is involved. In the type *A* fault generated surge system, good results have been obtained in this manner on phase-to-ground faults. Experience would indicate that the 50 per cent additional pulse travel required by the fault generated surge system would not greatly affect the precision of measurement.

5. It is true that the pulse generated by fault is likely to be highly variable in magnitude and perhaps also in steepness. However, the voltages involved are so tremendous that the chance that the pulse would be too small in magnitude is very remote. All pulses measured to date have been so very steep that precision should not be appreciably affected by any variation in steepness. However, the magnitude variation did raise a problem in the design of the equipment to provide proper gain setting for display of reflected pulses. This was solved by designing the circuits so that the gain is a function of the magnitude of the original pulse.

6. We agree with Leslie and Kidd that it is probably quite feasible to apply sufficiently high voltages for proper operation of the Linascope method on transient faults.

7. The discussion recognizes the advantage of the type *B* locator in the form of simplified presentation, but mentions that the cost will be prohibitive in most cases. This is true, but as mentioned in our paper, we believe that the number of places where channels will exist to which it can be applied at a negligible additional cost will ultimately be fairly large; and that where such conditions exist, the advantages will easily justify its use. The possibility of using transmission lines rather than radio relay system to transmit the timing pulse could well be investigated.

Spurious impulses will indeed operate the equipment and several false recordings will be received for each true fault location signal. Figure 18 assumes this. However, the latter can readily be recognized because it will coincide in time with the line relay operation. If desired, the chart recorder can be built so that relay operation will indicate on the chart.

In our paper we discussed the reasons why we first investigated the fault generated surge system, in preference to others, but did not attempt to rule out the artificially pulse systems. In fact, we suggested how one obvious difficulty (interference from fault transients) might be overcome. This suggestion has since been proved correct in tests reported by Spaulding in his discussion of the paper by J. R. Leslie and K. H. Kidd.[1]

In general, we view the present status of the problem stated in the following paragraphs. We consider the type *B* best, where applicable, because it makes the reading of fault location as simple and accurate as the reading of amperes and kilowatts.

There is also a field for another transient locator, perhaps a type *A*, perhaps a Linascope, perhaps some modification or new development. It will be installed in important lines where lack of a communication channel will rule out the type *B*. Attempts to eliminate photography and substitute indicating instruments should be continued so long as any practical hope of achieving this exists.

Finally, there is a field for a simple portable fault locator for locating permanent faults only, if it can be made cheap enough so it can be used where a permanent transient fault locator installation cannot be justified.

REFERENCE

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Magnetic Suspension of the Watt-Hour Meter Rotor

H. F. TREKELL
MEMBER AIEE

L. I. MENDELSOHN
MEMBER AIEE

J. H. WRIGHT
MEMBER AIEE

Synopsis: By supporting the weight of a watt-hour meter disk magnetically, a bearing system is achieved free from high-unit pressures and, hence, free from the fundamental cause of wear. The magnetic arrangement employs two concentric magnets of "cunico," the inner attached to the top of the shaft, the outer attached to the meter frame. In a meter having other design innovations co-ordinated with the magnetic suspension it promises to give virtually maintenance-free operation and long life.

THIS paper deals with a meter bearing system, magnetic suspension, which is radically different from those now in use. As a result of new materials and new techniques it is possible to make successful application of a concept which has intrigued designers since the early days of the watt-hour meter. Magnetic suspension offers the possibility of eliminating the high pressure inherent in mechanical bearings, pressure which reaches the order of 200,000 pounds per square inch and is the fundamental cause of wear in such bearings. While many improvements have been made over the years and present-day mechanical bearings give remarkable performance, the fact remains that bearings constitute a major cause for meter maintenance.

Mechanical Bearings

The history of the watt-hour meter is replete with progress and improvement in mechanical bearings. From the beginning the axis of rotation of the disk has been made vertical. In all bearing systems now in use, the lower bearing supports all the weight of the moving system and provides also alignment to maintain radial location of the lower end of the shaft. The lower bearing is made in two forms. In the pivot bearing it consists of a hardened pivot running in a jewel cup of sapphire. In most instances the pivot is attached to the shaft and the jewel is stationary, whereas in other meters an inverted construction is used. In the ball bearing, two cup-type jewels of sapphire are used, one jewel being stationary, the other attached to the shaft, with the hardened ball located between the two jewels.

Figures 1 and 2 illustrate both constructions as used in present-day meters. The top bearing functions only as a guide means and consists usually of a brass or bronze bushing attached to the shaft running on a stationary hardened and polished pin as illustrated in Figure 3. In some cases the pin is made long and flexible to provide a cushioning effect to allow disk vibration without excessive noise. In others, as shown in Figure 3, a vaseline lubricant is used to keep vibration to a minimum. In all cases, small clearances are employed.

Development and improvement of these mechanical bearings to their present state has included extensive investigation of the best in materials and lubricants as well as in the proportioning of parts.[1,2] The use of noncorrodible material for the pivot or ball or materials where any products of corrosion are nonabrasive has contributed to this improvement;[3] so also have superior lubricants[4] to give long life without oxidation, evaporation, and loss by creeping away, and the use of oriented sapphires[5] to take advantage of the best wear properties of the jewels.

These advances have extended bearing life to the point where it is common practice to check meters only once every eight years. However, the urge for improvement is ever present, since reductions in service and maintenance costs in an item as widely used as the watt-hour meter is significant in the operations of electric utilities. The long life inherent through the use of magnetic suspension of the watt-hour meter rotor promises to reduce bearing maintenance to a new low level.

Early Magnetic Suspensions

One of the earliest attempts at magnetic suspension was the ingenious construction used by Stanley in his magnetic flotation meter about 1903. Other proposed constructions during the last half century have resulted in several patents. In some cases magnetic attraction was used only to reduce the weight on the jewels but not entirely to eliminate it. In others, as in the case of the Stanley meter, a reluctance design was used to give complete support. Unfortunately, the earlier designers were limited by the lack of suitable permanent magnet materials making it necessary to use soft iron poles in conjunction with large permanent magnets to obtain the required lifting force. As a consequence, lifting forces were accompanied by potentially large side thrust forces due to flux crowding with any eccentricity or lack of symmetry. Resulting wear on guide bearings caused failure of the system.

Repulsion-Type Magnetic Suspension

In the early 1930's age-hardening permanent magnet alloys, since called the "alnico" group, became available. These alloys had much greater coercivity than had been available, thus making it possible to support a movable magnet by repulsion without the excessive mutual demagnetization which occurs with the

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H. E. Trekell, L. I. Mendelsohn, and J. H. Wright are all with the General Electric Company, West Lynn, Mass.

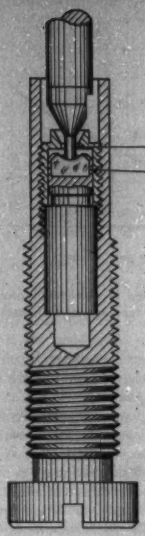


Figure 1. Pivot-type mechanical bearing

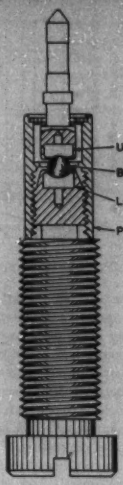


Figure 2. Ball-type mechanical bearing

materials of lower coercivity. The possibility of applying this repulsion property to supporting the meter rotor was recognized and samples were constructed. Figure 4 shows the relative arrangement of the lower or fixed magnet and the upper magnet which was attached to the rotor. This arrangement showed much promise since stable vertical equilibrium was established and side thrust forces were not excessive with reasonable radial displacement of the upper magnet with respect to the lower magnet. Later the aolly "cunico" (Figure 5) became available.[6] This represented a great advance because the material not only had high-coercive properties but it also was machinable whereas "alnico" could only be cast, sintered, or ground to shape. During the course of applying the "cunico" material to the repulsion-type suspension, a concentric arrangement of magnets was conceived. This offered so many advantages that all efforts were placed on it, resulting eventually in a practical solution of the long desire to support the watt-hour meter rotor magnetically.

Concentric-Type Magnetic Suspension

Figure 6 shows the arrangement and direction of magnetization of the "cunico" stator and rotor magnets of the concentric suspension as well as the associated flux distribution. Here, as in the repulsion system, the components are cylindrical so that the fields can be visualized in three dimensions, as a rotation of the 2-dimensional plots about the axis *YY*. The repulsion system (Figure 4) yields a large amount of leakage flux (*L*) which does not contribute to the lifting force, as only the flux immediately adjacent to *N-N* is effective.

In the concentric system, most of the flux is of value, and only a small percentage of leakage flux must be tolerated. Moreover, vertical force is provided at both ends of the concentric magnets by attraction between north and south poles.

Although both repulsion and concentric arrangements can be developed into practical systems, the decision to concentrate on the concentric system was made on the basis of the following facts:

1. The concentric suspension can be located at the top of the shaft, where it is remote from disturbing fields due to severe surge conditions in the current coils. In its best mechanical design the repulsion system must be located at the bottom of the shaft.

2. The concentric system results in lower radial or side thrust forces than the repulsion system for any given eccentricity of the rotor.

3. With materials now available, the concentric system is stiffer than the repulsion system.

4. The repulsion system must incorporate a shield to minimize locking torques with the neighboring magnetic materials (the damping magnet or the steel of the electromagnet). The concentric system is self-shielding.

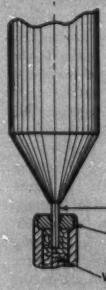
5. For equal lifting force, the rotor magnet of the concentric system is smaller in diameter than the rotor of the repulsion system; thus magnetic locking effects are inherently smaller in this design.

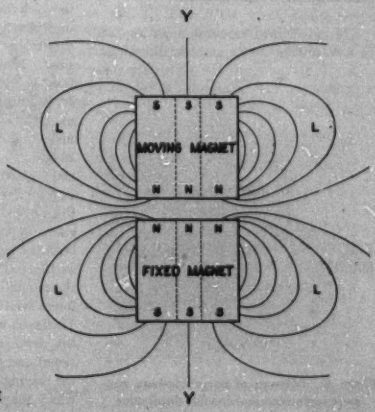
These facts are based upon experience with the most appropriate permanent magnet material available today, namely, "cunico." Possibly, as research discloses new permanent magnet materials, the repulsion system will find application.

In all practical magnetic suspension systems, downward displacement of the moving system produces an upward re-

Figure 3 (below). Upper guide bearing

Figure 4 (right). Repulsion-type magnetic suspension showing flux distribution





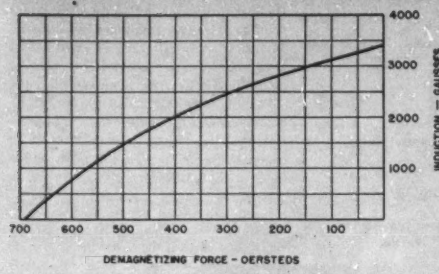


Figure 5. Demagnetization curve of "cunico"

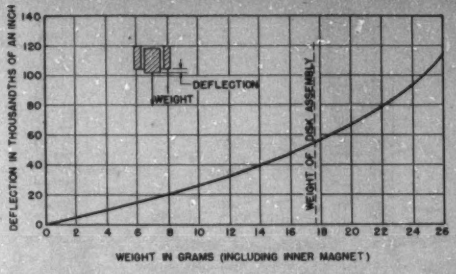


Figure 7. Vertical load-deflection curve of the concentric-type magnetic suspension

storing force, giving stable equilibrium in the vertical direction. Radial forces are balanced for a perfectly concentric system producing no net resultant. Off-center displacment, however, results in a lateral force tending to increase the eccentricity. Thus, a guide system is essential to maintain equilibrium, and assure concentricity with negligible radial thrust.

The vertical load-deflection curve of the concentric system is shown in Figure 7. At the operating point, which corresponds to the weight of the suspended system of 17.6 grams, the slope is 0.005 of an inch per gram. Experience has proved that this gradient or stiffness is more than adequate to assure a stable vertical position. The curve for radial thrust versus off-center displacement (Figure 8) shows that the concentric system approaches closely the ideal suspension. With an eccentricity of 0.002 of an inch allowed for manufacturing tolerances, the force is less than 0.3 gram.

Mechanical Construction

The concentric suspension as applied to a watt-hour meter rotor is illustrated in Figure 9. The disk and shaft are essentially conventional in construction and similar to other meter designs. The outer "cunico" magnet and upper guide pin are assembled concentrically by means of die cast construction. This assembly is adjustable for height in the meter frame. The inner magnet is attached to the meter shaft also by means of die casting.

Both magnets are machined to close dimensions from "cunico" rod stock. The die casting assembly technique and the close tolerances assure good concentricity. The guide pin in the outer magnet assembly engages the guide bushing of the inner magnet maintaining alignment of the top of the shaft. A similar arrangement is used at the lower end where the guide pin is part of another die-cast assembly adjustable for height. No lubrication is used.

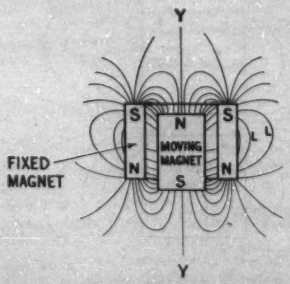


Figure 6. Magnets of concentric-type magnetic suspension showing flux distribution

Co-ordinated Meter Design

The concentric-type magnetic suspension inherently has practically unlimited life because of the extremely small forces on the running surfaces. The new meter in which the magnetic suspension has been incorporated has been designed with coordination to complement the suspension. The principal feature is the location of the braking or control magnets. In the conventional meter, the arrangement has consisted of an assembly of two magnets located at the front of the disk. In the new meter, there are two braking magnets which are cast as inserts in the frame, one on each side of the shaft. Figure 10 illustrates this arrangement, which has important consequences on both the side thrust and vibration forces in the rotor. Side thrust originates in the combination of driving and braking forces which act on the disk. In Figure 11, these forces are indicated for the magnet arrangements of both the conventional and the new meter. In each case the driving torque is produced by a force which may be considered to act on the disk at a point approximately in the middle of the electromagnet at the rear of the disk, Figure 12.

The braking forces act at the location of the braking magnets. In the conventional meter the braking force is in the same direction as the driving force. The moments of these two forces about the shaft are equal and opposite, but the resultant side thrust, which the bearing system must carry, is the sum of the two. In the new meter the braking forces act under the two side-located magnets to produce pure torque. The only side thrust is due to the driving force. At maximum load in the new meter the side thrust is only six grams, with one gram applied to the upper guide pin and five grams on the lower guide pin. Translated into pressure the maximum side thrust corresponds to a pressure of about 100 pounds per square inch. This is in striking contrast to the high pressure in a mechanical bearing, which is present at all loads.

The relocation of the braking magnets and the like polarity with which they are magnetized serve also to reduce vibration. In any meter there are two principal sources of disk vibration. One is due entirely to the potential flux and it takes the form of motion at 120 cycles per second (for 60-cycle supply) in a direction at right angles to the face of the meter. Being caused by potential flux alone it is unchanged in the new meter. The other vibration comes from interaction between the braking magnets and the current set up by the potential flux.

In Figure 13 it is analyzed for both the conventional and the new meter. The forces produced have a frequency of 60 cycles per second, and in the conventional meter they result in an oscillating couple, as the two magnets of the pair are of opposite polarity. This couple produces a lateral motion at the shaft, and in com-

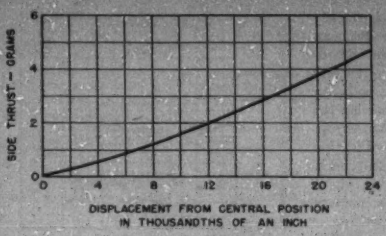


Figure 8. Side thrust due to eccentricity in the concentric-type magnetic suspension

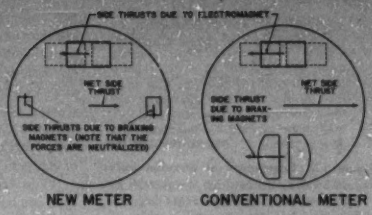


Figure 11. Comparison of side thrust forces due to meter operation

bination with the 120 cycle back-to-front force makes the mode of oscillation of the disk a "figure 8" pattern. With the magnets located at the side and magnetized in the same polarity, the 60-cycle vibration forces in the new meter are almost exactly opposite in direction and produce a negligible resultant.

The vibration patterns produced by these two conditions of operation may be graphically illustrated by a technique in which tiny mercury drops condensed on the blackened surface of the disk are photographed at high magnification.[7] In order to eliminate from the comparison any effects due to the bearing or suspension system itself, the procedure was adopted of suspending the disk assembly without appreciable sidewise restraint. Patterns obtained by this technique are shown in Figure 14. The lesser vibration inherent in the new design is obvious from the figures produced. Like the reduced side thrust, this lesser vibration gives the magnetic suspension more favorable operating conditions. It allows the guide pins to be shorter and stiffer than would otherwise be possible, making a more compact and practical design. At the same time, noise is maintained at an exceedingly low level at all loads and without lubrication.

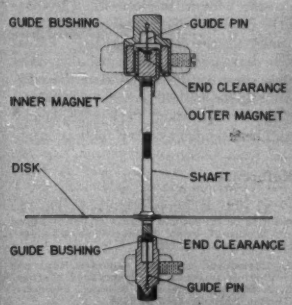


Figure 9. Cross-section of magnetic suspension in a watt-hour meter

Performance of the Suspension

The ultimate proof of the magnetic suspension to replace the traditional mechanical bearing must come from actual tests in meters. The usual operating characteristics, such as load and voltage curves, frequency, power factor, and temperature curves are not affected in any significant way. The suspension has some influence on starting watts. If the material of the magnets is not perfectly uniform magnetically, or if the magnets are not perfectly concentric with the axis of rotation, a small torque will occur in the suspension, which varies in magnitude and direction around one revolution of the rotor.

By maintaining close dimensional tolerances and rigid inspection of material, this effect is made small, but such torque tends in general to increase starting watts. Acting in large measure to offset this, however, is the improved concentricity of the disk about the axis of rotation. This is possible by the use of the guide bushing in the shaft to replace the pivot or jewel cup of the mechanical bearing. Reduction of disk eccentricity decreases an important item in the components which control starting watts.

It is natural to suppose that short-circuit currents and lightning surges in the meter might produce magnetic fields strong enough to affect the suspension. It is shown by extensive tests, however, that this is not the case. The suspension is located at the top of the shaft, where it is fairly remote from the current coils. Moreover, the magnets, besides being made of the high-coercivity material "cunico," are protected in appreciable measure by being encased in a die-cast housing of conducting material. Following general magnetic practice the magnets are stabilized also by a knock-down field after saturating. No significant weakening of magnets due to time, temperature, or heavy current surges has ever been detected.

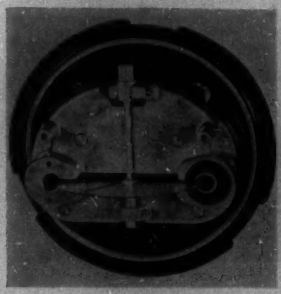


Figure 10. New watt-hour meter less cover and register

The die cast material over the right-hand magnet has been removed to show the relative position of the disk and braking magnets

Life Tests

In order to establish beyond a doubt the permanence of the suspension magnets, life tests have been in progress since 1941 in about 200 meters. They are operated at varying loads on a daily cycle, including light load, rated load, and 400 per cent load. This cycle gives about ten times normal registration, so that in $6^1/_2$ years the registration has totaled the normal registration of 65 years, or about 80,000,000 revolutions. The tests have

Figure 12. Rear view of frame, electromagnet, and disk assembly showing relative positions

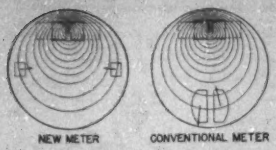


Figure 13. Comparison of vibration forces caused by eddy currents due to potential flux reacting with braking magnet flux

shown excellent stability for the magnetic suspension, for in all samples the disk height has been maintained and there apparently is no reason why this will not be so indefinitely.

At the outset it was realized that great care would probably be necessary to detect any effect of time and wear. Accordingly, check points were chosen that would reflect any change in friction, separate and distinct from other factors affecting calibration, such as changes in the effective position of any of the adjustments. The most significant value found was a quantity which has been designated "Δ" (delta), which is obtained by taking both forward and reverse readings at full and light loads. Expressing meter registration in per cent of true watt-hours it is given by

$$\Delta = \frac{\text{F.L. forward} + \text{F.L. reverse}}{2} - \frac{\text{L.L. forward} + \text{L.L. reverse}}{2}$$

Increase in friction at light load will increase this value, whereas other causes of calibration change will not affect it appreciably. An increase in delta will cause a corresponding decrease in light-load registration. To show the response of the life-test meters, changes in delta are plotted as a function of time and the meter registration in millions of revolutions.

The life test was designed as a proving ground for the proper guide-bushing materials, as well as a check on the stability of the magnetic suspension. Nineteen combinations of guide-bushing materials were included in the life test, all without lubrication. Early failure was found in one group. All other groups have maintained operation, and the results on the meters with five of the best material combinations are shown in curve *A* of Figure 15. This grouping includes 57 meters and the average change in delta is plotted. This curve shows the very satisfactory condition of only a small increase up to 0.13 per cent at the start of the test with a subsequent decrease so that at the last two observations, the friction is slightly less than at the start of the test. For comparison, curves *B* and *C* show corresponding data on standard pivot-type bearings and ball-type bearings. In both instances, with some variation during the life test, the general trend has been upward with an increase in delta or friction of 0.25 per cent apparent at the last check.

While the performance of the pivot-type and ball-type bearings is satisfactory and in line with good service experience, it must be recalled that these are accelerated tests and that the factor of time separate and apart from meter registration must have more influence on the pivot and ball-type bearings than on the magnetic suspension guide bearings where there is no question of lubrication and the bearing pressure is much lower. Thus, the superiority of the magnetic suspension apparent in these tests should be confirmed to a greater extent with extension of the test and in service.

With several alternatives in satisfactory bushing materials, choice of the production material could be based not only on the life test results, but also on suitability for manufacturing with the required precision, freedom from corrosion and oxidation for years, continued availability, and control of quality. Considering these factors and excellent experience on the life test, the combination of graphite guide bushings running on stainless-steel guide pins was chosen in preference to other satisfactory combinations which included various laminated plastics in combination with stainless steel. Graphite is very inert, can be machined readily with precision, takes on a smooth gloss without appreciable wear on the contact surface, and should last indefinitely.

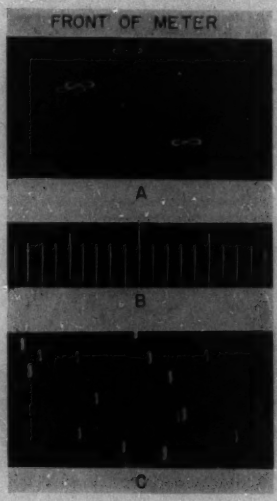


Figure 14. Disk vibration

A. Conventional meter
B. Scale showing magnification. One division equals one thousandth inch
C. New meter

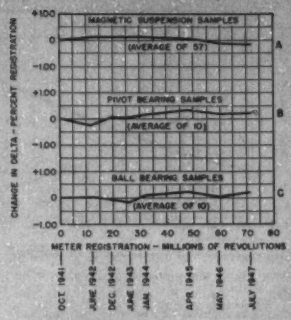


Figure 15. Results of life test on magnetic suspensions compared to mechanical bearings

Changes in delta are equivalent to changes in friction

Conclusion

The magnetic suspension described in this paper approaches the ideal for a watt-hour meter bearing system. Mechanical pressure at its highest is of the order of 0.05 of one per cent of that occurring in mechanical bearings. Materials of the guide pin and bushing, the only parts in physical contact, are noncorrodible and operate without lubrication. The magnetic material is highly stable and well protected against demagnetization. Extensive tests confirm the conclusion from theoretical considerations that the magnetic suspension will provide long maintenance-free operation. The principle may well be extended to other applications.

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Discussion

F. C. Holtz (Sangamo Electric Company, Springfield, Ill.): The presentation of this paper is both timely and stimulating, and the authors deserve much credit for having achieved a practical meter bearing construction which is a definite advance over the pioneer work of Stanley and others. The availability of new permanent magnet materials has lent considerable encouragement to the practical possibilities of this type of suspension, and it may well be the means for improving the reliability of meter performance over longer periods of time than are now generally accepted. The authors distinguish between two types of meter bearings currently being used, but in the writer's opinion, they do not stress sufficiently the higher degree of performance and reliability which has already been achieved by the ball bearing construction. Field tests initiated in 1940 on several hundred meters having ball bearing construction show that the average change in friction of the lower bearing after eight years of continuous operation under the varying conditions encountered in the field is less than one-tenth of one per cent. It is also to be pointed out that no lubrication of any type was employed in these meters either for the upper or lower bearings. The value of delta used by the authors for expressing meter registration in per cent is slightly misleading in that any variations in its value do not necessarily indicate change in friction. This is especially true considering the magnitude of the observed changes illustrated graphically in Figure 15.

Either positive or negative values may result from changes in the meter structure, the positioning of its adjustments or in the magnetic or electrical properties of the materials used in its construction.

In any event, it should be stressed that the variations in its value from time to time are the most important consideration. The authors imply that under actual service conditions, the factor of time would influence the ball bearing construction more unfavorably than the magnetic suspension. There is nothing in the record of performance of ball-bearing meters which could lead to such a conclusion. In fact, it must be admitted that we do not have, as yet, any appreciable background of experience with magnetic suspensions on which to draw such a conclusion. The superiority of magnetic suspensions is therefore not established, but it is hoped that it may represent a marked contribution to the problem of meter maintenance. It would be interesting if the authors could give figures showing the manner in which the values of delta on individual meters deviated from the mean in any one set of observations.

The magnetic suspension proposed by the authors is particularly susceptible to willful derangement and may become a source of annoyance to the industry. This fact should not be overlooked and some effort should be made to reduce such possibility. The authors illustrate, in Figure 8, the manner in which misalignment of the components of the magnetic suspension influence the side-thrust. Because of the very appreciable radius of action at points where the bearing surfaces are in contact, it would be interesting to know something about how they affect the value of delta and how this value changes with slight inclinations of the meter. It would also be interesting to have information as to the actual value of the friction of such a bearing system at low speeds of rotation, such as are experienced under light load conditions. As pointed out in the references cited, the figure eight loops shown in Figure 14, could be greatly reduced by proper choice of magnet materials and consequent spacing of the poles in the conventional design of damping systems.

A noticeable decrease in side-thrust under heavy load conditions has, however, been achieved in the damping magnet system used by the authors. This should have a tendency to reduce wear under load.

The authors mention bearing pressures of the order of 200,000 pounds per square inch on meter bearing systems. While this is, in general, true for the pivot-type of bearing, it is not true of the ball bearing construction where the pressures are of the order of 70,000 pounds per square inch, which are well within the practical limits of the materials used in their constructions.

Because of the many factors involved, it may well be that a compromise solution of the meter bearing problem may have possibilities. Experimental work on such constructions has been in progress for a number of years and shows excellent results.

A. R. van C. Warrington (General Electric Company, Philadelphia, Pa.): This paper is of interest to all designers of small devices such as relays which require a low friction level. It is of particular interest to relay engineers, because the increasing demands for speed, sensitivity, and accuracy are threatening to exceed the limits of present designs.

In the application of the watt-hour meter magnetic suspension to certain protective or regulating relays, the limiting factor would probably be the effect of the side-thrusts which increase at higher torque levels. It is at low torques, particularly near pick-up, that the principal benefits of the magnetic suspension will be realized.

The authors are to be congratulated on an excellent paper and an important contribution to the art.

H. E. Trekell, L. I. Mendelsohn, and J. H. Wright: The authors appreciate the kind remarks of the discussors and hope that the paper will serve to stimulate thought on other possible applications, as well as on meters and relays, where it is important that friction be maintained at consistently low levels throughout a long lifetime.

The authors would like a better criterion to measure changes in friction than changes in delta, but, where testing is necessary without removal of parts, no other factor has been found so significant. With a good bearing system, friction values are so small that detection of changes can be readily obscured by other causes of calibration change. On the other hand, it has been the authors' experience that these changes other than friction either are so small as to be negligible, or act in a way not to affect the value of delta.

It is the authors' belief that the magnetic suspension, properly applied, constitutes no significance as regards willful derangement. Possibly the concern in this respect was that it might be possible to demagnetize the suspension causing the disk to drop and drag on the air gap surfaces. This possibility has been eliminated in the construction used. At the bottom end of the shaft, and above the lower guide bushing, a polished end stone of sapphire has been placed. The lower guide pin is located in assembly with only a small clearance between the hardened stainless-steel pin and the jewel. In the event the suspension is demagnetized willfully, the jewel and the rounded and polished pin act as an excellent inverted pivot-type bearing, permitting the meter to continue to operate satisfactorily for many years. With little to be gained there should be little desire to demagnetize the suspension magnets.

Tests, where the meter assembly is tilted off the vertical, show that the magnetic suspension is superior to mechanical bearings because of the better alignment inherent with the guide pins.

Calculations of the stress in new mechanical bearings, using methods in use for years and cited in the authors' references, show the value for pivot-type bearings to be in excess of 200,000 pounds per square inch. The value for ball-type bearings ranges from 182,000 to 199,000 pounds per square inch depending on the weight of the single-phase meter rotor being considered. These values are calculated at zero load on the meter. The addition of side-thrust with increase in load increases the stress.

Effects of Silicone Vapor on Brush Wear

J. MARSDEN
NONMEMBER AIEE

ROBERT H. SAVAGE
NONMEMBER AIEE

THE lubrication of graphite by water vapor is now well known. Many other condensable vapors, however, have been found equally effective and substitute adequately for water. Striking demonstrations of this occurred during the high-altitude testing of aircraft brushes during the war. It was found that certain solvents from the freshly-painted interiors of cold chambers provided vapor lubrication for brushes operating under dry conditions, and brush dusting, which normally would have occurred, was prevented. The easily condensable vapors, as a class, excepting corrosive substances, thus gradually became recognized as lubricants for carbon brushes.

It was extraordinary, in view of this, to find in 1945 that the wear rates of carbon brushes were accelerated by a factor of ten or more when operated in totally enclosed motors having silicone insulation. The high wear could be lowered quickly simply by opening the machine to the normal atmosphere. Silicone-insulated motors which were not totally enclosed showed no such trouble. Initially, it was suggested that silicone vapor from the insulation formed water-repellent films on the commutator and interfered with the action of the water, but this explanation seemed improbable because the normal vapor lubricants for carbon do include water-repellent substances[1] for example, benzene and *n*-hexane.

Thus, some quite different mechanism had to be found to account for the unusual brush wear. This was the status of the problem when it was referred to this laboratory in November 1945.

Initial Apparatus

A few exploratory trials of carbon brushes operating in air under a bell jar quickly proved that the vapors distilled from typical silicone varnishes increase the wear rate by several fold. Special devices were then constructed to study the problem in detail. The initial apparatus consisted of a copper ring (23 millimeters in diameter), mounted on the shaft of a d-c motor (4,000 to 8,000 rpm) and carrying two electrographitized carbon brushes. The entire assembly was enclosed under a bell jar. The brushes were 6.2 millimeter square cross-section and were mounted under spring forces (300 to 400 grams) in radial box-type holders which were fastened to a water-cooled copper plate. Electric connections were made with tapered silver plugs, pressed into holes drilled in the brush sides, and soldered at the outer ends to copper pigtails which were connected to the voltage source. See Figure 1.

Initial Experiments: Effect of Methyl Silicones

With this apparatus, the effects of pure, volatile silicone compounds on brush wear were studied in detail. A typical substance, hexamethyldisiloxane,* was placed under the bell jar in a small beaker near the collector-ring and was found to increase the wear rate for current-carrying carbon brushes on copper from a normal 0.2 mil per hour to about 5 mils per hour. The accelerated wear occurred repeatedly in the presence of this vapor and also in the presence of other methyl silicone vapors (of both the straight chain and the cyclic types) providing the apparatus was enclosed with the vapor source or the atmosphere containing the vapor was recirculated over the brushes. Using individually a series of pure methyl silicone liquids of increasing vapor pressure, a measurable effect on the brush performance (contact voltage drop rise) was noted when the vapor pressure of the source was only 0.007 millimeter (mercury). The typical accelerated wear was obtained when this vapor pressure reached or exceeded about 0.2 millimeter.

This accelerated wear occurred only when the brushes carried current, approaching maximum intensity in the current range 10 to 20 amperes, a fact which is especially significant, as will be seen later. Furthermore, this wear was unlike that noted with brushes in high-altitude atmospheres where the wear-rate is steady and high. In the silicone-accelerated wear, the brushes passed through the following cycles:

1. There is an initial period of increasing voltage-drop frequently attended by sparking but with only slight wear.
2. There is a sudden fall in this drop simultaneous with a large increase in friction during which a cloud of black carbon wear-dust was blown from the brush faces.

This cycle repeated aperiodically as shown in Figure 2. This general effect was qualitatively reproducible, although the wear rates were not, the latter showing variations amounting to nearly one order of magnitude.

Another curious characteristic of the silicone-accelerated wear was a persistent "after effect" upon the brush performance following removal of the silicone

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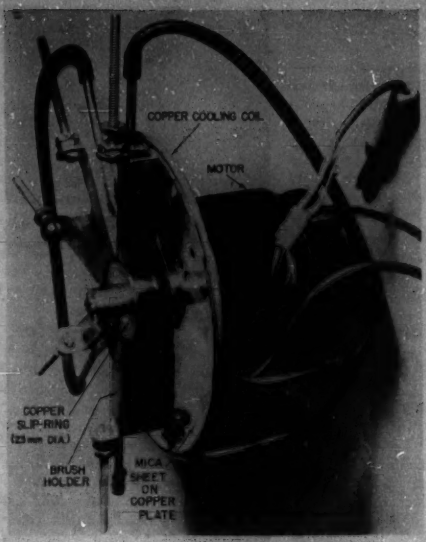
J. MARSDEN and ROBERT H. SAVAGE are both with the research laboratory of the General Electric Company, Schenectady, N. Y.

The writers are glad to express their appreciation for many helpful suggestions which have been made in the course of this work by their colleagues, especially those of C. Van Brunt, A. L. Marshall, and F. J. Norton. Thanks are due Charles Foster who carried out a large part of the experimental work with the resins and with the impregnated brushes.

* Vapor pressure = 40 millimeters (mercury) at room temperature.



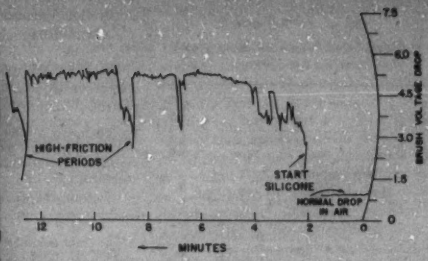


Figure 1 (left). Brush apparatus

Figure 2 (above). Brush voltage drop in methyl silicone vapor+air

source. This is illustrated by the following typical experiment.

After a run in silicone, the apparatus was opened and was aired for 65 hours. The carbon dust was removed by a compressed-air blast, and the brushes were then operated again with the bell jar still off (ordinary air atmosphere). For about ten minutes the operation was normal. Then suddenly the friction became high, and a cloud of wear dust was thrown from the slip ring, the brushes hissing as though rubbing against an abrasive. This occurred several times before the brush operation was again entirely normal.

The Wear Debris: Origin of the High Wear

The wear dust collected from intermittent dusting periods was examined after several individual experiments. These samples showed, in addition to the large quantity of carbon dust, some copper fragments evidently gouged from the ring. A few of these were several millimeters long, although microscopically thin, indicating that microscopic tools were producing severe abrasion in the contact zone. In addition to the copper fragments, there were found masses of minute thin white plates, some glassy and all closely associated with graphite. These white masses under the microscope were found to be infusible at temperatures up to 1,000 degrees centigrade and were indicated to be siliceous residues. The brush faces from the silicone experiments were found to show white particles concentrated in the zones of real contact (where specular polish had developed), as shown in the photomicrograph, Figure 3. These particles were not revealed until after the gloss had been removed from the brush face by gentle etching.

These, and other observations to be described later, suggested that silicone vapor is oxidized in the zone of interfacial brush contact to form siliceous residues which are apparently the primary cause of the high wear. The insulating films so formed can presumably be removed only by actual wear, their vapor pressures being negligible at the interfacial temperatures. Microanalyses of representative films formed on copper slip-rings and commutators, by carbon brushes operating in air containing silicone vapor, have shown excessive amounts of silica, which is simply the ash resulting from the oxidation of the vapor in the high temperature[3] of the sliding electrical contact.

Silicone Vapor as a Lubricant for Carbon Brushes

When brushes were operated upon a copper disk in vacuum equipment[1] with oxygen excluded, it was found that a silicone vapor (hexamethyldisiloxane), at a pressure of 4 millimeters (mercury), actually provided lubrication as does water in the normal case. No accelerated wear was noted, even under current density of 54 amperes per square centimeter. The voltage drop of the brushes was low and steady in these trials with silicone vapor only. When small amounts of oxygen were introduced, voltage drop showed an unstable rise; and subsequently, with higher oxygen pressure, high friction developed. Experiments of this kind showed that the silicone vapors are stable and actually lubricate graphite in absence of oxygen. When the brushes were operated in a nitrogen atmosphere (in an improved apparatus described in the following pages), it was found, consistent with this, that the silicone vapor again provided lubrication for the brushes.

Sorption of Silicone by the Apparatus

By accident, it was found that certain of the silicone vapors were sorbed in glass apparatus with a persistency similar to that shown by water. For example, following exposure to silicone vapor the apparatus was evacuated intermittently

Table I

Effect of Current[3] on Carbon Brush Wear in Air Atmospheres Containing the Silicone Vapor *$[(CH_3)_2SiO]_3$

Silicone Partial Pressure, mm Hg	Brush Current, Amperes	Wear Rate, Mils Per Hour
0.0	15	0.08
0.3	0	0.08
0.3	3	0.3
0.3	6	0.2
0.3	15	8.7

* $[(CH_3)_2SiO]_3$: Vapor pressure 0.3 millimeter at room temperature. The vapor pressure data for the silicones used in this work were taken from an unpublished report by D. Wilcock dated Aug. 26, 1946.

Table II

Effect of Silicone Vapor on Brush Wear in Various Atmospheres[3]

Experiment	Time of Run, Hours	Atmosphere: Gas	Atmosphere: Silicone Vapor	Wear Rate, Miles/Hour
A	3	$Air+H_2O$	none	0.1
B	4	N_2+H_2O	$[(CH_3)_2SiO]_4$	0.08
C	3	N_2+H_2O	$[(CH_3)_2Si]_2O$	0.1
D	7.8	N_2	$[(CH_3)_2Si]_2O$	0.12
E	3.25	N_2	$[(CH_3)_2Si]_2O$	0.12
F		$Air+H_2O$	$[(CH_3)_2SiO]_3$	8.72
G	15	$Air+H_2O$	$[(CH_3)_2Si]_2O$	13.4

for a period of several days to a pressure below 0.01 micron. Even after this prolonged exhaust, the glass continued to evolve the silicone when the pumps were shut off. Starting at a pressure rise of about 0.5 micron within the first few minutes, the pressure would continue to increase to as much as ten microns silicone vapor in the course of a few hours. This provided a serious source of contamination for brush experiments because of the cumulatively contaminating effects of silicone vapor on brush wear. To avoid this trouble completely, it was necessary to bake out parts of the apparatus.

Some Difficulties with the Initial Apparatus

Although the slip-ring apparatus shown in Figure 1 was useful in providing the foregoing preliminary information, it was not possible to reproduce actual wear-rates in silicone experiments with it, but only qualitative effects. After considerable work, it was decided that this lack of reproducibility was due, at least partially, to a nonequilibrium condition of the silicone vapor in the brush enclosure and to unusual variations in the brush currents.

The latter variations resulted from the abnormally large changes in brush contact resistance which were imposed in a low-voltage circuit. These were the unexpected results of drastic and irreversible contamination of the contact surfaces by the silicone. (It was found in this connection that the voltage fluctuations constituted the most sensitive indication of the presence of silicone vapor in the system, as will appear later, and always preceded the period of incipient wear). The variations in the brush current were easily overcome by connecting the brushes into a higher voltage power-potentiometer, but the variations in the vapor pressure made necessary the building of new apparatus.

Controlled Vapor-Pressure Experiments

The improved apparatus designed by one of us (Marsden) consisted of two parts:

1. There was a train of bubblers through which a carrier gas was passed to supply a known concentration of silicone to the brush enclosure.

2. The other part was a compact enclosure containing the brush and slip-ring device.

The train consisted of an all glass system having, in series, a compressed air or nitrogen source, flow-meter, three bubblers containing a pure liquid polysiloxane (silicone) compound of known vapor pressure, and finally three bubblers containing a saturated water solution of $MgCl_2$.* This led directly to the brush enclosure. The brush assembly was similar to the apparatus initially used, except that a larger motor (1/4 horsepower 4,150 rpm direct current) was substituted, and this was separated from the brush atmosphere by a brass plate fitted with a felt gasket around the motor shaft. Instead of the large bell jar, a small soapstone cylinder was mounted on the plate and was covered with a glass window. This chamber was fitted with neoprene gaskets and was maintained at a pressure equivalent to about 2 millimeters (mercury) above atmospheric.

With this new apparatus, and using a silicone liquid of somewhat lower vapor pressure to moderate the accelerated wear, it was possible to repeat experiment with reasonable consistency.

The general effect of current on the brush wear in this apparatus is shown in Table I. The large increase in the wear rate obtained at the high currents may be understood as the result of increased oxidation of silicone in the contact zone with a greater attendant rate of production of abrasive products.

The explanation of the primary cause of the wear, which has been given is clearly supported by the foregoing results, and also by a separate series of experiments in which nitrogen, purified over hot copper, was substituted for air and passed into the chamber with the silicone vapor. See Table II. These results show that brushes may be operated at a normal (that is, negligible) wear-rate in the presence of silicone vapor if oxygen is excluded. Water vapor apparently does not affect the lubrication in this case.

* 30 per cent relative humidity at 30 degrees centigrade. The humidity was carefully controlled to insure uniformity of atmospheric conditions. However the water vapor concentration within the normal range of atmospheric variation was not found to have an important effect on the silicone-accelerated wear.

Figure 3. Photomicrograph of brush face, showing white masses in the contact zone, after etching

Phenyl Silicones

After concluding the experiments with methyl polysiloxane liquids, several attempts were made to obtain comparative data substituting pure phenyl silicones. Because the vapor pressures of the phenyl compounds are extremely low at the apparatus temperature, it was not possible to pass the vapor into the enclosure without rapid condensation and the attendant uncertainty of the effective concentration of the vapor in the brush ambient. Experiments should be made along these lines with the apparatus heated to prevent condensation. The writers have not found the opportunity to do this. However, a number of trials have been made using brushes impregnated with phenyl silicones and with methyl silicones, respectively, for comparison purposes. After considerable work, it is concluded that this is probably not a reliable method of evaluating a silicone effect, although high wear has been produced by some of the silicones by this means. A few interesting facts however should be noted.

The wear rates (1 mil per hour) of some methyl silicone impregnated brushes containing 4.8 per cent impregnant were especially significant when compared with those (8 mils per hour) of brushes in a methyl silicone vapor, $[(CH_3)_2SiO]_3$, particularly in view of the fact that unimpregnated brushes of the type used were found to sorb only 0.2 per cent by weight even after 18 hours in this vapor at room temperature. These results show that the total amount of silicone sorbed by the brush is not an important factor. What is fundamentally important is the rate at which vapor molecules impinge upon the surface of the commutator and at the brush face.* These molecules have a short adsorption life, of the order of

* Using kinetic theory formulae, the calculated weight of a silicone $[(CH_3)_2SiO]_4$ impinging on unit surface in unit time is 0.02 gram per square centimeter per second.

micro seconds,[4] but this time is seemingly sufficient to allow fixation and oxidation of the sorbed silicone to take place.

Effect of Vapor from Silicone Resins Upon Brush Wear

Summarizing the foregoing experiments, it was indicated that pure, volatile, silicone vapors of low molecular weight increase the wear-rate of current-carrying brushes operating in air by forming abrasive siliceous products. This undoubtedly is the key to the origin of the high wear shown by brushes on actual machines, totally enclosed, where the source of the vapor is the hot insulation. There we may expect products (from thermal cracking) similar to the pure compounds which have been shown to initiate the high wear under a bell jar. In order to throw more light on this possibility, and to apply the principles derived from the studies with pure compounds to actual resin applications, a parallel study was made of brush wear in the presence of hot insulation.

A preliminary study using a hot, organic insulation-varnish showed that the vapors from this material tended, if anything, to decrease brush wear-rates. The subsequent work was therefore confined to the silicone resins, having the reverse effect.

The apparatus was similar to that described under "Initial Experiments" except for minor changes which permitted the continuous passage of air over the brush system from auxiliary equipment outside.

The vapor from the hot insulating resins was passed to the brushes by the following procedure. A controlled air stream (160 cubic centimeters per minute) was passed through a series of four saturated magnesium chloride bubblers, then through a glass tube which contained the strips of resin-coated glass tape* and which was enclosed in a muffle furnace, and finally, through heated glass tubing directly to the brush track. This was intended to duplicate qualitatively the totally enclosed motor set-up in which the cooling blast, for the commutator, streams across the hot armature, carrying along the volatile components from the windings of the machine to the brush ambient.

With this equipment a long series of experiments was made[3] having the objects of determining the effect of the "resinous vapor" upon the wear, and of finding whether the accelerated wear could be prevented by a more complete curing of the resin. For this purpose the brushes were tested in the vapor evolved by standard-cured resins which had been obtained from each of two manufacturers of silicone insulation, and in the vapor from supercured resins, baked at temperatures in excess of those required to effect a normal cure.

The data showed that all of the resins, when heated above 175 to 200 degrees centigrade, increased the wear rates of the brushes. They showed also that the brush wear, caused by the vapor which was evolved by heating a typical resin at 250 degrees centigrade, was reduced by prior supercuring of the resin, effected by heating at 250 degrees centigrade for 16.5 hours. After additional supercuring at 250 degrees centigrade for a total of 52.5 hours, no additional reduction in brush wear was noted, and at the end of this time the brush wear was still four to eight times greater than normal wear. The silicone-coated resin tape continued to lose weight, and in the latter stage of heating there was no change in the slope of the weight loss curve. This general effect was found for all of the resins which were studied and may be regarded as especially significant in view of the fact that the supercuring was sufficient in some instances to reduce the useful insulating life of the resin by an estimated 65 per cent.

Weight loss data for the resins which were heated at 250 degrees centigrade for a rather long time (56 hours) were taken. The data showed that the slopes of the weight-loss curves, both in air and in a nonoxidizing atmosphere (nitrogen), were nearly identical. This fact indicated that little oxidation occurred when the resin was heated in air. On the basis that oxidation of two methyl groups leads to the formation of an oxygen bridge, the weight loss data indicate that less than 2 methyl groups per 100 silicone atoms are oxidized.

It is suggested from this work that the continuing weight loss, even after heating for a long time at 250 degrees centigrade, is due not to the removal of volatile material originally present in the resin after preparation, but rather due to the removal of volatile fragments produced by thermal decomposition of nonvolatile polymer. This result is particularly important because it indicates that accelerated brush wear will persist until the useful insulating life has been largely destroyed.

Solutions for the Problem

We are thus led to a consideration of means to overcome the accelerated wear other than by the supercuring of the resin (although that is a partial aid). The experiments of one of us (Marsden) showed that brushes, which had been impregnated with certain lubricant liquids, could be operated in air atmospheres containing silicone vapor for extended periods without showing accelerated wear. In these experiments it was found that, under heavy current loading, these brushes gradually lost their impregnant by evaporation as a result of which the wear-rates gradually became excessive. It is suggested that with more research along these lines a longer term solution to the problem might be obtained.

However, a quite different approach to the problem is already under engineering development; namely, the isolation of the brush system from the atmosphere containing the traces of silicone vapor, by enclosing the commutator and brushes in a separately pressurized compartment so that the vapors cannot reach the brush zone of rubbing contact. This procedure appears to provide a practicable and effective solution to the problem.

It is to be noted that the high wear produced by silicone vapors in air atmosphere has not been found to occur on machines which are ventilated, so that apparently no problem openly exists for most motor applications of these high temperature insulations.

Conclusion

1. In the presence of oxygen, and in a confined space, methyl polysiloxane (silicone) vapors are found to accelerate the wear of current-carrying carbon brushes several-fold (and up to 800-fold under the severe conditions of high silicone concentration coupled with high-current density).

2. This high wear occurs as a series of intermittent dusting periods, each lasting for only a few seconds, and each preceded by excessive build-up in the contact voltage drop, due to an irreversible contamination of the rubbing interface by silicone decomposition products. The momentary dusting period cleans off the insulating layer and exposes fresh surface for a repetition of the buildup and dusting cycle.

3. It appears that the most important step causing the wear is the oxidation of silicone to silica, or to a siliceous residue, which occurs in the zone of interfacial rubbing contact.

4. In the absence of oxygen, silicone vapor is stable and actually lubricates the graphite at a pressure of a few millimeters (mercury). This occurs independently of the presence or absence of water vapor.

5. Brushes impregnated with silicones

* 25 to 30 strips (12.5 centimeters long) were stacked loosely in a weighed glass tube parallel to its axis. The resin had been cured by standard procedure and was representative of that which is used on commercial totally enclosed d-c motors. The weight of resin was estimated from the weights of equal lengths of bare glass tape and resin-coated tape, and corresponded to about ten grams per charge.

(liquids or solids) may show the accelerated wear when run in ordinary air, but their use does not provide a reliable criterion of the wear that occurs when nonimpregnated brushes are operated in air containing silicone vapor.

6. The vapors, produced by heating, of each of the silicone resins investigated caused excessive brush wear. These resins included the principal commercial types currently available.

7. Long-term curing of these resins does not eliminate entirely the loss of volatile material, and so does not eliminate entirely the excessive brush wear which occurs when these resins are heated at high temperatures within the brush ambient.

8. It is shown that brushes impregnated with certain organic liquids (not silicones) do not wear at an excessive rate in silicone vapor until most of the impregnant has been evaporated from the brush as a result of high-temperature operation. It is suggested that further work with impregnated brushes might yield a longer term solution to the wear problem.

9. The most practical solution appears to be the isolation of the brush system from the silicone vapor evolved from the machine windings by means of a separate pressurized compartment in the totally enclosed unit.

10. No accelerated wear problem has been reported to exist in silicone-insulated machines which are not totally enclosed, so that the solutions outlined under (8) and (9) need be applicable only to the relatively small but important number of machines in which vapors from hot insulations are recirculated within the brush ambient.

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No Discussion

The True Dielectric Breakdown Strength of Electric-Cable Insulating Papers

PAUL CLOKE
FELLOW AIEE

BURT BATES
NONMEMBER AIEE

THE dielectric breakdown of electric-cable insulating papers has been the subject of intensive study over a period of many years. One of the major difficulties has been the elimination of the edge effect.

In the case of glass, this was accomplished by blowing thin glass spheres and testing them between mercury electrodes. This insured that the glass would break down through the thin portion and thus give the true strength of the glass.[1] Use was made also of special semiconducting baths when flat specimens were tested. Moon and Norcross used both of these expedients in testing glass, celluloid, and mica.[2]

Whitehead[3] tested electric-cable insulating papers by spiraling 1-inch papers on a brass tube in much the same manner as is done in electric-cable manufacture. He tested a built-up wall of insulation tapes with oil gaps between turns.

The purpose of this paper is to present a new and unique way of eliminating edge effect and obtaining results which give the true breakdown strength of the paper, with a single voltage wave. Some breakdown strengths obtained by other investigators, with different test conditions, are given for comparison.

Apparatus

The apparatus consisted of a specially shaped electrode and a saturated paper sample, with build-up paper to fit the surface of the special electrode. The equipment for applying the voltage (Figures 1 and 8) consists of a 15-kva 220-to-150,000-volt testing transformer which may be connected on the low-voltage side to a capacitor, which is charged through a kenetron. On the high-voltage side, the terminals are connected through a limiting resistance and inductance to two 0.012-microfarad capacitors in series which, in turn, are in series with a 1-microfarad 1,000-volt capacitor. The three capacitors are paralleled by the sample of paper to be tested. The oscilloscope (persistent screen type —45 seconds) is connected to the 1-microfarad capacitor on the high-voltage side of the transformer.

Method

The capacitor in the low-voltage circuit is charged to a sufficiently high voltage to give a puncture of the paper on the high-voltage side. This capacitor is charged, and then discharged into the low-voltage winding of the transformer. The deflection of the beam on the oscilloscope, at which the paper breaks down, is easily read.

Computations

Since the oscilloscope was calibrated by both a standard direct and an alternating sine-wave voltage (which checked very well), it was possible to compute the total voltage across the sample from the known capacitances of the capacitors in the high-voltage circuit. The capacitances were determined with a standard bridge.

The Special Electrode

The unique feature of this investigation is the specially shaped electrode, the design of which is described subsequently. This entails a new physical structure by which edge effect is eliminated on sheet material, such as paper. Moon and Norcross, and Whitehead used methods which were electrically equivalent to the one used in this investigation. Moon and Norcross[2] used thin glass spheres filled with, and immersed in, mercury, as well as flat specimens in semiconducting baths. Whitehead[3] used test specimens consisting of 16 layers of 5-mil cable paper, one inch wide, spiraled cable-fashion on a smooth brass tube, one inch in diameter, 48 inches long, with 33 1/3 per cent overlap and nominal 3/64 inch channel width.

Empirical Relation

In designing the special electrode which will be described, it was assumed that the dielectric strength of the paper in the transverse direction was approximately 50 times greater than in a longitudinal direction. This was the result of experience in cable testing.

The results showed that between 55.9 and 66.3 per cent of the punctures occurred on the flat, central part of the electrode.

Design of Special Electrode

Referring to Figure 2 (not to scale; see Figure 3), the bottom electrode E is a highly polished, circular, flat disk. The paper sample is represented as P, and the special electrode as S.

The electrode S was made in two sections; the first was perfectly flat for a distance of 1/2-inch radius from the center, and the second half was machined according to the curve derived below.

The basic equation used in the derivation is as follows:

$$\frac{b}{t} = K \qquad (1)$$

Where

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PAUL CLOKE is dean of the College of Technology, at the University of Maine, Orono, Me. BURT BATES is also of the College of Technology at the same university.

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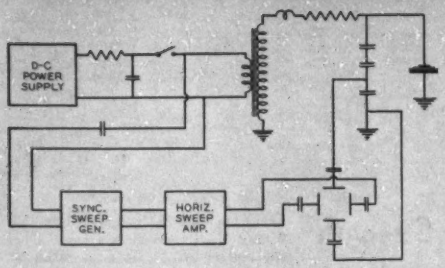

Figure 1 (left). Electrical circuit diagram

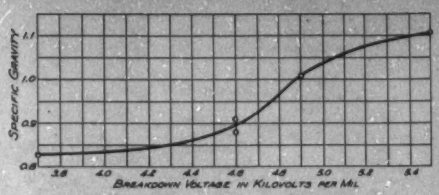

Figure 5. Specific gravity versus breakdown voltage

b = the angle, in radians, between the tangent to the curved portion of the electrode and the horizontal

t = the thickness of the insulation plus the thickness of the reinforcement, in mils

K = a constant

At the point where the curved portion of the electrode is in contact with the flat portion, the cotangent of b is taken as 50. The value of the derivative at any point on a curve is equal to the tangent of the angle at which a tangent line, drawn through that point, meets the horizontal. Stated mathematically

$$\frac{dy}{dx} = \tan b \qquad (2)$$

If the insulating paper to be tested is approximately 5 mils thick, then

$$t = y + 5 \qquad (3)$$

When cotangent $b = 50$, $b = 0.02$, and $y = 0$. Substituting these values in equations 1 and 3 gives $K = 0.004$.

Combining equations 1, 2, and 3 gives,

$$dx = \frac{dy}{\tan K(y+5)} = dy \cot K(y+5) \qquad (4)$$

The integrating of dx between 0 and x, and $f(y)$ between 0 and y in equation 4 gives

$$x = \frac{1}{K} \ln \sin K(y+5) + C \qquad (5)$$

where

C = constant of integration, and when evaluated, by letting $y = 0$, is found to be 978.

This gives the following equation of the curved portion of the electrode:

$$x = \frac{1}{K} \ln \sin K(y+5) + 978 \qquad (6)$$

Figure 3 shows the actual dimensions of the curved portions of the special electrode. The formula derived is that which is required to keep the tangential stress below the known tangential, dielectric strength of the reinforcement in insulation.

Figures 2 and 4 are enlarged to clarify the design features. They are not to scale.

Build-Up Paper

The build-up paper should be of very thin rice paper, or preferably, of the same kind of paper as the sample to be tested, and sandpapered down to fit the curved portion of the special (upper) electrode.

The function of the build-up paper is to confine the puncture to the central, flat portion of the special electrode, and thus puncture the paper samples. The build-up paper and special electrode are the really unique features of the investigation. Even if edge effect were entirely eliminated, some breakdowns would undoubtedly occur outside the central area because of variation in the paper. For the majority of tests, the build-up sheets were made of 1-mil paper. The build-up sheets were cut with an outside diameter of three inches and with varying sizes of circular holes in the center. The diameters of the holes in inches are as follows: sheet 1, 1.000; sheet 2, 1.080; sheet 3, 1.162; sheet 4, 1.232; sheet 5, 1.288; sheet 6, 1.344. The total build-up paper was made by gluing sheet 2 on sheet 1, sheet 3 on sheet 2, and so on, by means of a small drop of glue on the outer edge of the sheets, where the glue would not come in contact with the electrode. When the total build-up paper was made, the shape of the electrode was approximated by the depression formed by the holes in the center of the build-up paper.

In some cases, sheet 1 was omitted. For some tests, the insulating-paper samples were used as build-up paper by cutting a 1-inch, or a $1^1/_8$-inch hole in the center of the paper, and tapering the center circle by use of fine sandpaper. Another variation was the use of half mil paper for build-up. In this case, 11 sheets were used for the build-up paper. The inside diameters in inches for these sheets are as follows: sheet 1, 1.000; sheet 2, 1.040; sheet 3, 1.880; sheet 4, 1.121; sheet 5, 1.162; sheet 6, 1.197; sheet 7, 1.232; sheet 8, 1.260; sheet 9, 1.288; sheet 10, 1.316; sheet 11, 1.344.

In all cases, enough sheets were used to give a total thickness of build-up paper of at least five mils. All paper samples,

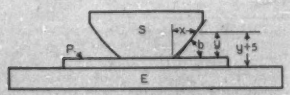

Figure 2. Special electrode

Figure 3. Plot of curved portion of electrode

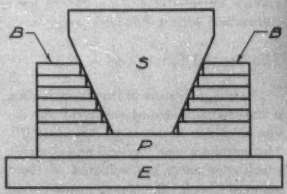

Figure 4. Special electrode and build-up paper

Figure 6. Wave form

Timing wave 600 cycles
Time to crest 0.000556 second
Time to half voltage from start 0.0033 1/3 second

Table I

Samples	Breakdown, Volts Per Mil	Specific Gravity
B	3,700	0.827
C	4,600	0.880
E	4,800	0.909
A	4,900	1.010
D	5,500	1.106

including the build-up papers, were dried for 24 hours at 105 degrees centigrade and one millimeter (mercury) of pressure. The samples were then impregnated with transformer oil at 105 degrees centigrade.

Breakdown Test

All samples were tested under oil in an earthen crock. The oil was continually filtered by a circulating pump. The crock was kept well covered, when not in use, so that very little dirt could get in the oil.

The breakdown tests were short in duration, but a great amount of care was necessary to measure the breakdown voltage accurately. The oscilloscope was connected across capacitors in the voltage divider circuit of such a value that the deflection on the oscilloscope would fill the screen. The capacitance was kept constant at 1.03 microfarads during the investigation.

Build-up paper was placed on the sample and carefully centered. The combination was placed on the electrode, and the top electrode adjusted so that its curvature was in the build-up paper. This was one of the most difficult parts of the test to perform accurately. The electrode holder was then lowered into the crock.

When the paper sample was adjusted properly, the capacitors in the low-voltage transformer circuit were charged by the variable d-c power supply, to a potential high enough to cause breakdown.

After all preliminary adjustments were made, the capacitors were discharged through the low-voltage winding of the transformer. This caused a high voltage in the high-voltage winding, breaking the paper. The following data were recorded for every run: the direct voltage to which the capacitors, connected to the low-voltage winding of the low transformer, were charged; the position of breakdown; the vertical deflection of the beam on the oscilloscope; and the value of capacitance across the oscilloscope.

Figure 5 shows the relation between breakdown in volts per mil and paper density. Specification of polarity is not essential since the stresses at the two electrodes are essentially the same.

Table II

Samples	Breakdown, Kilovolts Per Centimeter	Volts Per Mil
Glass	4,500	11,400
Celluloid	2,500	2,500
Mica	10,600	26,900

Distribution

A study of the distribution of breakdowns over the face of the one-inch, central portion of the special electrode shows that the actual distribution checks quite well with the theoretical, computed distribution on the basis of annular, concentric zones of equal area.

Figure 6 shows the wave form of the puncture voltage. The timing wave is 600 cycles.

As a result of the tabulation and comparison of data obtained in this investigation, the following conclusions were reached:

1. The special electrode and build-up paper made possible the determination of the breakdown strength under surge voltage for single thicknesses of oil-impregnated paper.

2. The build-up paper is necessary, and the best results were obtained when build-up paper was made from the sample paper.

3. The breakdown voltage obtained in this investigation is about twice as great as that found by other investigators.

4. The range of breakdown voltage was quite wide, but seemed reasonable for dielectric strength tests.

5. The actual percentage of breakdowns which occurred within the central 1-inch portion was fairly close to the theoretical value. This is on the basis of concentric zones of equal area on the central portion of the special electrode. Rather a high percentage of breakdowns, however, occurred outside of this area, as would be expected statistically. As has already been pointed out, breakdowns would occur outside the one inch center, even if edge effect were entirely eliminated.

6. The breakdown voltage increases with increase of specific gravity. Data, obtained in our tests, are seen in Table I.

Some Comparisons

Moon and Norcross[3] obtained the results shown in Table II in the disruptive region, with applied direct current, using mercury electrodes for thin glass or semiconducting baths for celluloid and mica.

Whitehead and Kopper[2] found the values for insulating papers, as shown in Table III.

In comparing these tests with data published previously concerning paper or

Table III

Breakdown, Volts Per Mil		
60-Cycle RMS	60-Cycle Peak	Specific Gravity
713	1,008	0.83
653	922	0.86
573	810	1.14
564	798	1.10

Table IV. Summary of Breakdown for Various Sections of the Electrodes for the Five Samples

Position	Number	Per Cent of Total
Sample A		
1	10	13.0
2	12	15.6
3	21	27.3
4	22	28.6
5	6	7.8
6	6	7.8
Total	77	
Total for positions 1, 2, 3	43	55.9
Range volts per mil	3,400–5,700	
Average volts per mil	4,900	
Sample B		
1	3	5.3
2	4	7.0
3	17	29.8
4	26	45.6
5	4	7.0
6	3	5.3
Total	57	
Total for positions 1, 2, 3	34	59.6
Range volts per mil	3,000–4,600	
Average volts per mil	3,700	
Sample C		
1	1	1.8
2	8	14.3
3	25	44.7
4	14	25.0
5	4	7.1
6	4	7.1
Total	56	100.00
Total for positions 1, 2, 3	34	60.7
Range volts per mil	3,700–5,600	
Average volts per mil	4,600	
Sample D		
1	13	16.3
2	11	13.8
3	29	36.2
4	24	30.0
5	1	1.2
6	2	2.5
Total	80	
Total for positions 1, 2, 3	53	66.3
Range volts per mil	4,600–6,300	
Average volts per mil	5,500	
Sample E		
1	6	17.6
2	5	14.7
3	10	29.6
4	11	32.4
5	2	5.9
6	0	0.0
Total	34	
Total for positions 1, 2, 3	21	61.9
Range volts per mil	4,200–5,200	
Average volts per mil	4,800	

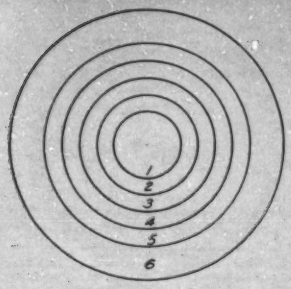

Figure 7. Method for determining position of breakdown

Table V. Breakdowns (Volts Per Mil) in Each of the First Three Positions

Number of Specimens	Position 1	Position 2	Position 3	Average
Sample A				4,900
10	4,970			
12		4,800		
21			4,875	
Sample B				3,800
3	3,530			
4		3,850		
17			3,830	
Sample C				4,720
1	5,400			
8		4,800		
25			4,660	
Sample D				5,560
13	5,700			
11		5,270		
29			5,610	
Sample E				4,600
6	4,715			
5		4,740		
10			4,460	

Table VI. Breakdowns (Volts Per Mil) for All Positions 1, 2, 3 for 6 and 5 Layers of Build-Up Paper, and for Build-Up Paper Made of the Sample Paper

Layers of Build-Up Paper	All Positions: Number of Specimens	All Positions: Average	Positions 1, 2, and 3: Number of Specimens	Positions 1, 2, and 3: Average
Sample A				
6	24	4,820	11	4,930
5	30	4,700	15	4,670
A	20	5,018	15	5,050
Sample B				
6	34	3,730	10	3,740
5	11	3,640	11	3,770
B	6	3,830	6	3,830
Sample C				
6	13	4,900	8	5,080
5	7	4,500	2	4,250
C	36	4,570	24	4,650
Sample D				
6	17	5,230	4	5,220
5	3	5,270	3	5,270
D	40	5,615	39	5,600
Sample E				
6	20	4,565	10	4,680
5	0		0	
A	14	4,600	11	4,620

build-up-paper insulation, it must be recognized that the quantities measured are not the same. These tests have developed the specific or inherent (short time) strength of the saturated paper. Tests on sheets or tapes in which the edge effect is not controlled give results that are a function of various things not intrinsic to the paper. Tests on build-up insulation are related to the structure and to the properties of the space between tape edges; specifically, Whitehead shows that the failure in his samples is a function of the stress in the oil between tape edges. Moreover, in comparing the numerical values of these tests with tests at 60 cycles the effect of time must be considered.

One of the most interesting comparisons that can be made is with Whitehead's results. The relative numerical values of his tests and these are essentially a measure of the different voltage schedule and of the fact that, in his type of sample, the test is essentially a measure of the strength of the oil film. It is for that reason that he obtains decreasing strength of the insulation with increasing density of the paper. In the present tests, the fact that the true strength of the paper is developed results in increasing strength with increasing density.

Halperin and Shanklin[4] report 1,330 volts per mil in cables with thicknesses of 125 to 600 mils with a $1^1/_2$–40 wave. They also report 2,400 volts per mil adjacent to the conductor.

One manufacturer names values of 1,200 to 1,800 volts per mil (rms, 60 cycles per second) as minimum average values in material specification for paper. These are based on American Society for Testing Materials (ASTM) specification *D-149*. The electrodes used are square ended, 1/4-inch diameter cylinders. The voltage is increased at the rate of 250 volts each five seconds until failure. The paper is dried and impregnated with oils such as used with solid-type cables, having a viscosity of around 100 seconds at 210 degrees Fahrenheit.

The standard ASTM sheet tester gives breakdown values as high as 2,500 volts per mil (rms 60 cycles per second) on high-density draft paper (specific gravity 0.95). This is equivalent to a peak of 3,500 volts per mil. Since the sample is 5 mils thick and the voltage is raised at the rate of 500 volts per second, the time of voltage application is 25 seconds. For an impulse application of voltage in 0.8 millisecond it would be expected that the

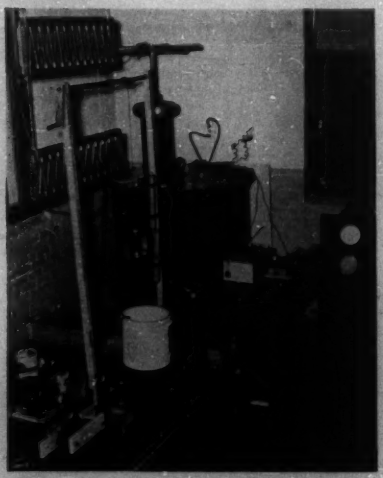

Figure 8. Equipment

Figure 9 (below). Oven and vacuum tank

breakdown value would be 30 per cent or higher. As between the ASTM samples and our tests, there are three fundamental differences. One is our elimination of edge effect; another difference is that one is 60 cycles and the other a short time surge; and the third difference is in the size of the specimen.

As far as the authors are aware, no reports have been made giving surge strengths exceeding 3,000 volts per mil. The difference in our results is likely due either to reasons such as discussed by Whitehead or to edge effect as in ASTM procedure. In Table IV, the total for positions 1, 2, and 3 give the breakdowns occurring on the central 1-inch diameter flat portion of the electrode. Figure 7 shows the sections of the electrode referred to as "position" in Table IV.

60-Cycle Tests

Sixty-cycle tests were considered before the project was started, also ASTM tests. While these results might give added interest to the paper they are not essential in fulfilling the primary purpose of the investigation.

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Discussion

C. E. Betzer (Commonwealth Edison Company, Chicago, Ill.): The results of this investigation are astonishing in that they show breakdown strengths of oil-impregnated paper insulation of 4,800 to 5,000 volts per mil. These strengths are for single thicknesses of paper having specific gravities of 0.9 to 1.0, such as those commonly used in cable insulation, and were obtained under voltage surge. They are considerably higher than the strengths reported by other investigators and are ascribed by the authors to the elimination of edge effects in the test cell. However, the extent to which this development is the explanation is not clear because no comparable results of tests with other test cells are given. The results of tests by other investigators given under the heading "Some Comparisons" were all obtained under some different conditions as to type of specimen, thickness of insulation, type of voltage applied, and time to break down.

These high strengths under voltage surge suggest that some improvements in the methods of using impregnated paper insulation might be possible to take advantage of the high inherent strength. At present the limiting value used in design applications for built-up insulation is about 1,200 volts per mil.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): Data relating to the inherent dielectric strength of impregnated paper, one of the most important high-voltage insulating materials is shown here for perhaps the first time. It has been shown previously (by Whitehead) that, where a series of overlying saturated paper tapes are assembled into an insulating wall, the dielectric strength (under his test conditions) is a function not of the electrical stresses in the tape itself, but of the stresses in the oil-filled valleys between tape edges. Electrical failure in these oil spaces is propagated into the bordering tapes, and final dielectric failure occurs even though the stresses in the tape just prior to the oil breakdown are far lower than the strength of the tape as determined by Cloke by these newly presented data.

It is thus perfectly consistent that, whereas in Whitehead's specimens, the higher the density of the paper, the lower the dielectric strength, in Cloke's specimens, increasing density is accompanied by increasing dielectric strength.

These data form an important contribution to our understanding of the dielectric behavior of paper insulation. Knowledge of the basic properties of the materials with which we deal may be expected to be of importance in the development of high-voltage products.

G. M. L. Sommerman (Northwestern University, Evanston, Ill.): Workers in the field of dielectric breakdown strength will appreciate the painstaking care which Cloke and Bates have exercised both in the preparation of samples, and in the analysis of results in this investigation on single thicknesses of cable-insulating paper. Also, they will recognize the contribution of an evaluated method for reducing the predominance of edge failures in breakdown tests on sheet insulation. Whether or not the factor of sample preparation time will permit the

use of the new method as a quality control test remains a question.

The authors' explanation of the increase of breakdown strength with paper specific gravity in their tests is a reasonable one, but the high values of their reported breakdown strengths (3,700 to 5,500 volts per mil) call for additional discussion. Some relevant results were obtained by the writer in an investigation conducted during the summer of 1948 at the Westinghouse Research Laboratories. Some of the samples tested consisted of paper pressboard 0.125 inch thick, 0.95 specific gravity impregnated with Wemco C transformer oil. The electrodes were similar in area to those of Cloke and Bates, but the edge effect was practically eliminated by different methods. The average breakdown strength of seven such samples was 2,100 volts per mil with standard $1^1/_2 \times 40$ microsecond surge voltages. This agrees closely with the maximum surge-voltage strength of 2,400 volts per mil obtained by Halperin and Shanklin[1] and is less than half the value of 4,750 volts per mil shown in Figure 5 of the paper under discussion. It should be emphasized that no paper-free gaps existed in these samples, the only practical differences in the two sets of tests being those of sample thickness and of surge-voltage duration. The latter difference cannot account for the discrepancy, because the longer surge duration employed by Cloke and Bates would reduce rather than raise the breakdown voltage compared with standard surge results.

This leaves only (1) the factor of sample thickness, or (2) the possibility of an error in voltage measurement to account for the discrepancy. With respect to (1) it is, of course, well known that measured breakdown strength decreases as sample thickness increases, but the functional relationship is not very steep for surge strengths of high-quality insulation. In this regard it would be of value if the authors could extend their investigation to cover the effect of sample thickness up to the limit determined by their test voltage source. With respect to (2) it would be desirable for the authors to calibrate their capacitance-divider-oscilloscope measuring circuit by means of a sphere gap or a resistance-divider connected to the oscilloscope through the customary cable and impedance-matching termination. Otherwise there is the possibility that surge reflections at the oscilloscope terminals may lead to measured results perhaps twice as great as the correct values.

REFERENCE

1. See reference 4 of the paper.

E. W. Greenfield (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): The authors have presented an interesting study of the dielectric strength of single sheets of impregnated insulating paper.

The idea of building up end re-enforcements by means of specially cut and sanded-to-fit paper sheets glued together is certainly ingenious. However, this proposal would seem to require considerable exercise of skill and a great deal of time so that it probably could not be used for routine testing.

A number of years ago the discusser derived the equation for the slope of re-enforced insulation for cable terminals and

Figure 1. Fiber insulation re-enforcement and electrodes for measuring dielectric strength of sheet insulating materials. Slope designed for a uniform tangential stress of 5 volts per mil

also applied this derivation to the re-enforcement for a paper dielectric strength tester. In this derivation, the solution is based logically upon solving for the tangential stress at the re-enforcement boundary and then forming such a slope which would keep the tangential stress below the known tangential dielectric strength of the re-enforcement insulation. For the cylindrical electrode shape of a cable, this slope turns out to be a log log function. For the parallel plate cell used for paper testing, the slope turns out to be a log-transcendental function and is steeper. Figure 1 of the discussion shows the paper tester with machined permanent re-enforcement made of fiber, into which the shaped upper electrode rests. This type end re-enforcement was found to be moderately successful. No oil immersion bath was required and its use permitted rapid testing.

It is felt that the paper is weak in its comparisons with the work of others. All data should be reduced to a common basis, for example, maximum stress in volts per mil, peak. It would also seem that more emphasis should be placed on the comparisons to be obtained with the standard ASTM test electrodes (1/4-inch flat ended cylinders) rather than with Whitehead's work which was on a cylindrical cable structure, not paper sheet and was a life test.

The standard ASTM sheet tester gives breakdown values as high as 2,500 volts per mil (rms 60 cycles per second) on high density kraft paper (specific gravity 0.95). This is equivalent to a peak of 3,500 volts per mil. Since the sample is 5 mils thick and the voltage is raised at the rate of 500 volts per second, the time of voltage application is 25 seconds. For an impulse application of voltage in 0.8 millisecond it would be expected that the breakdown value would be 30 per cent or more higher. Therefore the question as to what figure represents the "true breakdown voltage" needs to be carefully defined with regard to the conditions of test.

The statement as to the statistical distribution of breakdown is not clearly given in the paper. The tabulated data indicate that most of the breakdowns occurred through the third and fourth layer of the build-up paper. Since the radial stress at this region is only 1/4 to 1/3 the value at the central section, the tendency to fail most frequently through the re-enforcement at this position must be due to tangential stress. Hence "end effects" appear not to be completely eliminated. Also since failures in the re-enforcement are apparently counted in the resultant average, the authors should make it clear that the re-enforcement papers must be of exactly the same material and treatment as for the insulation under test. There will be many who regard failures in the 1-inch diameter single layer area as the only true radial failures achieved by this apparatus.

R. J. Wiseman (The Okonite Company, Passaic, N. J.): Reading of this paper by Cloke and Bates makes one feel that only a part of a research has been made. The test method used seems to be a very good one, particularly the manner in which the shape of the electrostatic field is handled. The desire to get as many breakdowns in the center of the electrodes is quite good and seems to have been accomplished to a high degree. If the authors intended to present a paper describing a method for getting better tests when making dielectric strength tests of impregnated paper, it would seem as if this has been accomplished, provided the sample was tested in an oil medium, as that is the way that the paper is used.

I think the authors should be very careful in making a comparison of the results obtained with results reported by others. Unless the paper is carefully read, one might say that the tests are at variance with others. It must be kept in mind that they are reporting results at probably switching surge frequency, yet they do not say so and I think they should. In fact, I think the correct title to the paper should have been The Dielectric Strength of Impregnated Paper at Switching Surge Frequency. It is not impulse frequency of $1^1/_2/40$ wave nor 60-cycle power frequency. Also they are testing one sheet of paper, whereas actually in a cable we have a multiple number of papers and a gap between turns of each tape. This gap introduces a different dielectric medium which changes the electric stress. This has been pointed out by Whitehead. I wish the authors had made single sheet tests at 60 cycles to prove to us that the higher the density of the paper, the higher the dielectric strength and then take three sheets of paper with a slot about 30 mils wide in the center sheet to simulate the manner in which impregnated paper is used and obtain voltage breakdown figures to prove that that which Whitehead found is correct, namely, that because of the oil in the paper gap and the difference in the specific inductive capacitance of the paper and oil, a lower dielectric strength is obtained as the density of the paper increases.

Let us hope that the authors or someone else will continue this study along the foregoing lines.

Paul Cloke and Burt Bates: Comments by the various people who have discussed the

paper and by the reviewers indicate that 60-cycle tests should have been made. As is clearly stated in the paper, it is hoped that at some future time this may be done. Also some question is raised as to the accuracy of calibrations. Suffice it to say that such a contingency was fully anticipated, and repeated checks and calibrations were made so as to relieve all doubts at least in the minds of the authors. It should be pointed out moreover that in the time available, since the work was done as a graduate thesis, only the tests reported could be made. It is believed that they are of very high accuracy.

Another discusser states that unless the paper is carefully read, the wrong impression might be created. The reply to this is obvious—"Anything worth doing is worth doing well."

In regard to the comment that the authors should be very careful in making comparisons with results of others, let it be stated as was done in the paper, that there is no intention of discrediting any work previously done.

The previous results were put in as is stated so that one might see readily what they were without the necessity of referring to quite a number of other papers. The results obtained under the conditions specified by other investigators are doubtless accurate and true.

Analysis and Redesign of a Carbon Pile Voltage Regulator for Aircraft Generators

W. B. KOUWENHOVEN
FELLOW AIEE

G. J. THALER
ASSOCIATE AIEE

THE carbon pile voltage regulator used on aircraft is an electromechanical device designed to maintain constant generator voltage under the varying loads and speeds encountered. The speed requirement is especially severe as aircraft generators are driven from the main engines and thus are subjected to a speed range of at least two to one.

The basic components of the carbon pile regulator are shown in Figure 1. They are an electromagnet, a spring, and a carbon pile. The electrical circuit of which these are a part is shown in Figure 2. The carbon pile is in series with the generator field and is compressed by the spring unit. The electromagnet is connected across the terminals of the generator and the force that the magnet exerts on its armature opposes the pressure that the spring applies to the carbon pile. Variations in generator output voltage produce changes in the magnet force and thus change the pressure applied to the carbon pile. The regulator is provided with three adjustments: the pile screw; the position of the magnet core; and a rheostat connected in series with the electromagnet. The most important of these is the pile screw which is adjusted, when the regulator is placed in operation, until the proper generator voltage is obtained.

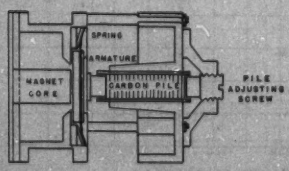

Figure 1. Construction of carbon pile voltage regulator

The principles underlying the operation of the carbon pile regulator are simple; nevertheless, performance has not been altogether satisfactory It was felt that it was desirable to understand thoroughly the basic operation of the regulator itself, and to separate it as far as possible from effects that might arise because of the characteristics of the circuit in which the regulator was used. Therefore, this paper is limited to an analysis of the regulator. Simplified equations are developed for the regulator and are applied both to the analysis of a typical commercial model and to the problem of design. On the basis of this analysis, the regulator was redesigned with a minimum of changes. Two new models were built. These showed greatly improved performance when tested.

The performance characteristics of the commercial regulators are shown in Figure 3, where the variation in generator voltage is plotted against the position of the pile screw, as the latter is turned clockwise, compressing the carbon pile. At first, the voltage rises rapidly from D to C as the pile screw is advanced. At C, a regulator usually becomes unstable, that is, the spring-armature unit begins to vibrate through an appreciable amplitude, producing rapid generator voltage swings. The amplitude may be sufficient to cause the disks in the carbon pile, particularly at the ends, to break contact with each other causing arcs to form between the disks and destruction of the disks. As the pile screw is further advanced, a point B is reached where the vibrations of the spring unit stop and the regulator becomes stable in operation. From B to A, the regulator is stable, and this narrow adjustment range is the region of normal operation. When placing a new regulator in service, the pile screw should be adjusted to provide the minimum voltage in the region B to A. If the pile screw is advanced still further, a rapid rise in voltage beyond A results, because the spring-armature of the magnet is now clamped tightly against the face of the magnet, and further advancement of the pile screw simply serves to compress the carbon pile. The regulator is inoperative, and the generator voltage will build up as in a shunt generator.

A study of Figure 3 shows clearly, that in adjusting the regulator for operation, it passes through a region of instability with the possibility of damage to the disks in the carbon pile, that the operating range is small and the location of the operating point critical. In service, the carbon pile wears, and its length decreases, which is equivalent to backing off the pile screw, that is, moving from A toward B on the voltage characteristics. Eventually, the unit becomes unstable, and the carbon pile rapidly disintegrates, due to arcing, unless a new adjustment of the pile screw is made.

In order to obtain information for an accurate analysis of regulator operation, a number of regulators were studied experimentally. The force versus air gap (armature deflection), for the magnet, and the spring force versus deflection were carefully measured, and also calculated from the known dimensions and characteristics of the regulator. On Figure 4, magnet force versus deflection curves for the average regulator, at sev-

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W. B. KOUWENHOVEN is dean of engineering and professor of electrical engineering at The Johns Hopkins University, Baltimore, Md., and G. J. THALER is assistant professor of electrical engineering at the University of Notre Dame, Notre Dame Ind.

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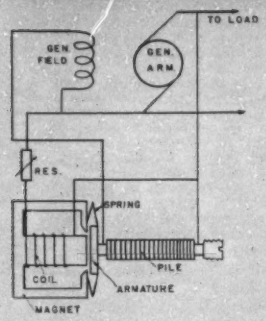


Figure 2 (left). Electrical circuit using carbon pile voltage regulator

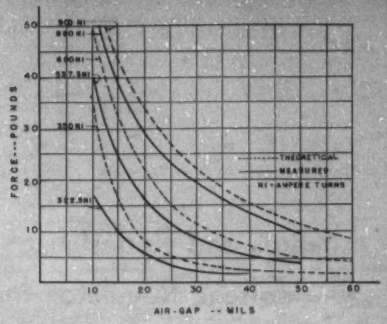


Figure 4. Typical force versus air gap characteristics of regulator magnet

eral values of ampere turns (generator voltage) and, in Figure 5, the force versus deflection characteristics for the springs which are firmly attached to the armature of the magnet are shown.

Force versus resistance and force versus deflection (compression forces) characteristics were measured for a large number of carbon piles, and their average characteristics determined. Typical carbon pile force versus deflection and force versus resistance characteristics are shown in Figure 6. In addition, the air gap, the net force on the carbon pile, and the behavior of the pile in actual operation were measured by special jigs and fixtures designed and built for the work.[3] It was found that in normal stable operation of the regulator, the net force on the carbon pile does not exceed two pounds.

Analysis

For the purpose of this discussion a simplified mathematical analysis of the carbon pile regulator is presented, which serves to explain its operation. In this analysis the characteristics of the external circuit are neglected, and attention is focused on the force equilibrium relations of the mechanical elements of the regulator.

The lack of consideration given to the effects of the external circuit on regulator operation is justified when the regulator operating requirements are considered. The first requirement imposed on any regulator is that it be stable and operate satisfactorily under conditions of constant generator speed and load. The second is that it should re-establish stable performance after a change in load or speed and minimize fluctuations during the transient period. In this second case, the type of external circuit will influence regulator operation and may result in hunting. This does not change the fact, however, that the regulator characteristics should of themselves aid in the restoration of stable operating conditions and that any unbalanced forces set up in a regulator by a transient should tend to produce force equilibrium. It is of interest to note that in none of the tests was there any evidence of hunting.

When the load and speed of the generator are constant, the resultant pressure on the carbon pile and its resistance are also constant if the generator output voltage is to remain fixed. The construction of the regulator is such, Figure 1, that under these conditions, a condition of force equilibrium between magnet, spring, and carbon pile exists as shown by equation 1.

$$F_s = F_m + F_{cp} \tag{1}$$

where

F_s = force exerted by the spring
F_m = force exerted by the magnet pull on its armature
F_{cp} = force exerted by the carbon pile.

Likewise, from the construction of the regulator, a displacement or movement of any one of the component parts (magnet armature, spring or carbon pile) must be duplicated by the others. This relationship is expressed by equation 2.

$$\Delta gm = \Delta ds = \Delta dcp = \Delta x \tag{2}$$

Where

Δgm = incremental change in magnet air gap
Δds = incremental change in spring deflection
Δdcp = incremental change in carbon pile length
Δx = incremental change in displacement (general)

An incremental change or displacement may be caused either by adjusting the pile screw, or by a variation in generator voltage caused by a change in load or speed, or by an external mechanical shock. Regardless of the cause of displacement, equation 1 must be satisfied both before and after the transient period.

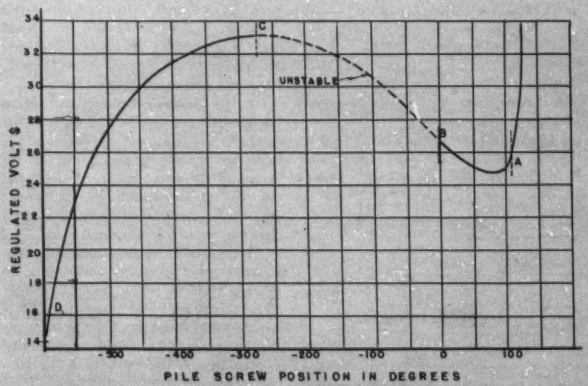


Figure 3. Typical performance characteristic of commercial regulators

During the transient period restoring forces should be set up if the regulator is to return to a new stable operating position. This condition is a function of the first derivative of equation 1 with respect to displacement. The value of this differential equation depends upon the particular characteristic of the component parts and three relationships are possible as shown by equation 3.

$$\frac{dF_s}{dx} \lesseqgtr \frac{dF_m}{dx} + \frac{dF_{cp}}{dx} \qquad (3)$$

Case 1. When conditions are such that the rate of change of the spring force with respect to displacement is less than the sum of rates of change of the other two forces with displacement, a set of unbalanced forces exists in the regulator. This condition was found experimentally to result in unstable operation. When these unbalanced forces are present, the magnet will immediately tend either to draw its armature to the pole face or to release it completely. Assume, for example, that the displacement transient is such that the armature is clamped against the pole face. This will cause a lowering of the pressure on the carbon pile and the generator voltage will fall until the spring takes over and drives the armature to its other extreme position. The pressure on the carbon pile will now be a maximum and the generated voltage will build up until the magnet once more asserts itself and draws the armature to the pole face. This will set the spring armature unit into vibration and will result in unstable operation of the regulator.

Case 2. Unbalanced forces also exist in the regulator when the rate of change of the spring force with respect to displacement is greater than the sum of the rates of change of the magnet and carbon pile forces with respect to displacement. In this instance, however, the unbalance apparently is in the form of a restoring force which opposes further displacement, and the regulator is stable as was borne out by the experimental work. For example, assume a transient displacement away from the magnet face, then both magnet and spring forces are decreasing. Under these conditions, however, the rate of change of the spring force with displacement is greater than that of the magnet force, and there exists an excess magnet force tending to restore the spring-armature to its original position. In cases where the transient displacement is in the opposite direction, that is, toward the magnet force, then both magnet and spring forces are increasing, but here the spring force is increasing more rapidly, and it provides the restoring force.

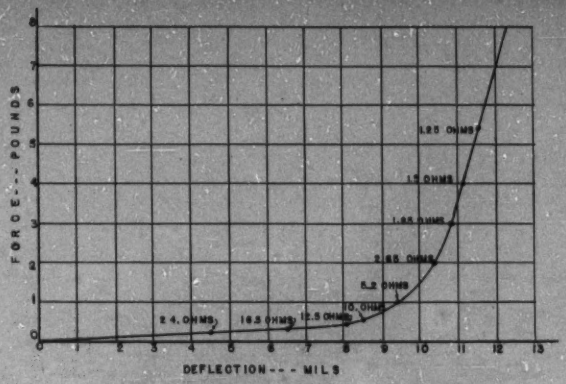


Figure 6. Force versus deflection characteristic of a typical carbon pile

Disk thickness 0.5 millimeter, over-all length 1.5 inches

Case 3. There is a third possible case where equilibrium, as signified by the equal sign of equation 3, may be maintained under all conditions, even transient ones. Under these ideal conditions, no force unbalance with displacement exists and the regulator is stable.

Graphical Analysis

In order to analyse the operating and the adjustment characteristics in terms of the mathematical treatment, it is necessary to make a large scale plot of the individual force deflection characteristics of the magnet, spring and carbon pile. In plotting such a set of curves, displacement or deflection is used as the abscissa and force as the ordinate. The method of locating the origins is as follows: maximum magnet air gap (armature deflection) occurs when the spring is at zero deflection; this displacement is measured and the origins of the two curves are separated by this measured distance. The common ordinate is force, and it is evident from the construction of the regulator that the force exerted by the magnet opposes that of the spring.

The carbon pile force deflection characteristic must now be added to this composite graph. The location of this characteristic will depend upon the adjustment of the pile screw. When the pile screw is advanced, the carbon pile characteristic is displaced from the origin of the spring curve toward that of the magnet curve. The spring characteris-

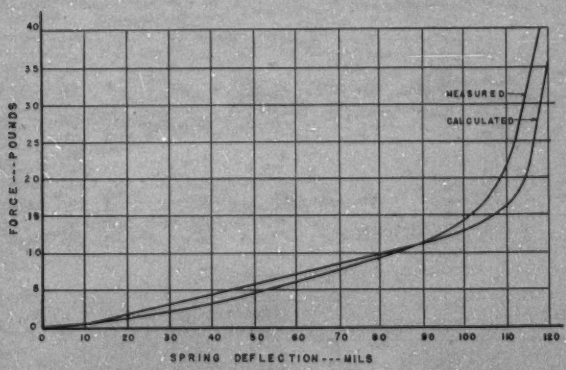


Figure 5. Typical force versus deflection characteristic of regulator spring

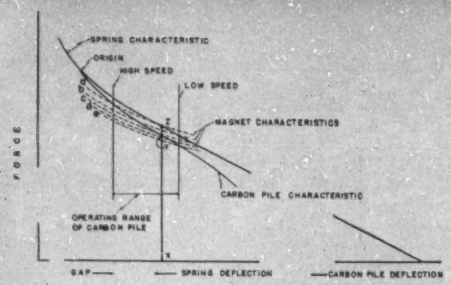


Figure 7 (left). Composite plot of magnet, spring carbon and pile characteristics

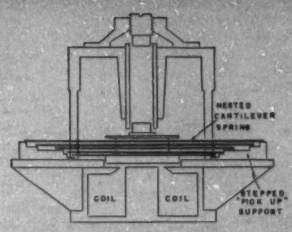


Figure 9. Sectional view of regulator with nested cantilever springs

tic, therefore, serves as a reference for the location of the carbon pile characteristics.

Figure 7 shows a sketch of such a large scale plot for the normal operating range (B to A of Figure 3) of a typical regulator. In Figure 7, are a group of magnet force deflection curves (broken lines) for different values of ampere turns (generator voltages) varying from a to e, a spring, and a carbon pile force deflection characteristics. The origin for the carbon pile is located on the spring curve, as described previously. It should be noted that the slope of the spring force deflection, dF_s/dx, is greater than that of the magnet curves for the range shown.

In Figure 7, the operating range of the carbon pile regulator is marked by two vertical lines. The actual location of these limits may be determined either by calculation or by measurement. The line XYZ within this operating range gives a graphical example of the addition indicated in equation 1. The spring force is XZ, the magnet force XY, and that of the carbon pile YZ. The location of the point Y determines the generator voltage corresponding to the given magnet ampere turn characteristic C.

The voltage regulation for the conditions shown in Figure 7 is given by the relation:

$$\text{Per cent regulation} = \frac{\text{Voltage of "a"} - \text{Voltage of "e"}}{\text{Voltage of "a"}} \times 100$$

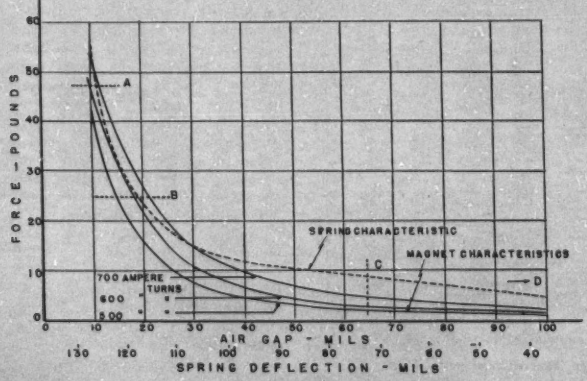
A composite plot of the actual magnet and spring characteristics of a given regulator is shown in Figure 8. To prevent crowding, the origin of the spring curve is not shown in the figure. The carbon pile curve is also not shown because it could not be separated from the spring curve at the scales used in this plot. This plot may be used to explain the adjustment characteristics of the regulator shown in Figure 3.

Both Figure 3 and Figure 8 are indexed with the letters A, B, C, and D for easy reference. If it is assumed that the operating point starts at D, the voltage is zero, as the carbon pile screw has only been advanced to the point where it just does not make contact with the carbon pile disks. As the pile screw is advanced, the operating point moves along the spring curve from D to C. In this range, the spring curve cuts across magnet characteristics of increasingly greater ampere turn values, and the generator output voltage rises in order to establish the values of F_m required by equation 1. Figure 3 shows that the voltage rises as the pile screw is advanced from D to C. Also, in this region, the slope, dF_s/dx, of the spring force deflection curve is greater than that, dF_m/dx, of the magnet curves. This is a characteristic for stable operation as shown by equation 3 and is confirmed by experiments.

As the pile screw is advanced from C to B, the spring curve recrosses the magnet characteristics, this time from higher to lower values of ampere turns, and the voltage of the generator will decrease. This is confirmed by experiment as shown by Figure 3. It should be noted also that in this range, the slope, dF_s/dx, of the spring curve is less than the slope, dF_m/dx, of the magnet characteristics and that this is a criterion for unstable operation. In the tests, the regulator operation was found to be unstable in this region.

As the pile screw is still further advanced from B to A, the spring and the magnet curves are at first parallel, and then the spring curve begins to cut across magnetic characteristics of higher values of ampere turns. This will result in a range of constant voltage followed by a rise. This is also in line with the experimental results shown in Figure 3. In this region dF_s/dx is either equal or greater than dF_m/dx and the operation of the regulator is stable as proved by experiment.

Figure 8. Composite plot of actual magnet and spring characteristics



Redesign

In view of the experimental and theoretical results, and consideration of production tolerances, it was decided to redesign

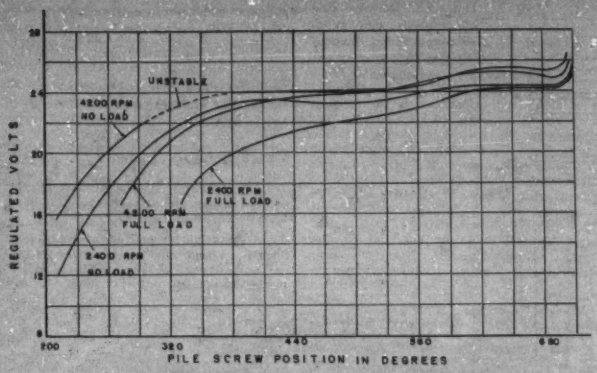


Figure 10. Performance characteristic of regulator with nested cantilever spring

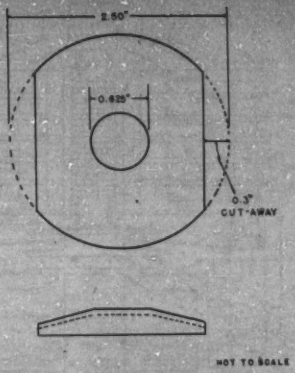


Figure 11. Belleville-type spring with cut-away sections

the regulator to satisfy *Case 2*, namely that

$$\frac{dF_s}{dx} > \frac{dF_m}{dx} + \frac{dF_{cp}}{dx}$$

and at the same time to make the spring and magnet force characteristics parallel over as wide a range as possible. Such a design has the advantage that the generator output voltage will be practically constant over the entire pile screw adjustment range and the regulator will be stable in operation.

Before constructing a model regulator to verify the conclusions experimentally, it was decided that the desired conditions could be obtained by redesigning only the spring. Therefore, two different types of springs were designed to match the existing magnet. The first spring was a nested cantilever which approximated a single magnet curve with five straight lines. This spring was not difficult to design, but was bulky, difficult to assemble properly, and extremely sensitive to dimensions and tolerances. The second spring was a Belleville washer type,[8] consisting of two coned disk washers in parallel. The design of this spring is comparatively, very difficult, and an exact match with a magnet curve was not obtained. However, after designing the Belleville spring to give approximately the desired force versus deflection curve, it was found possible to correct the discrepancies by cutting off two pieces from each disk along a chord.

Figure 9 shows a sectional view of the regulator with nested cantilever spring. The five layers pick up at different points throughout the deflection range. Figure 10 shows the regulator adjustment characteristics obtained with the nested cantilever spring. The speeds, 4,200 rpm and 2,400 rpm respectively, are the maximum and minimum rated speeds of the generator used. The curves show nearly constant voltage over a wide range of pile screw position, as predicted, and instability occurs only over a small portion of the adjustment range, and only at maximum speed, no load (broken line section of curve).

Figure 11 shows the Belleville-type spring with cutaway sections indicated. Two such units were used in the regulator, and were operated in parallel with the cuts aligned perpendicular to each other. Figure 12 shows the regulator adjustment characteristics obtained with the Belleville springs. These compare well with the characteristics obtained with the nested cantilever spring, and although not quite as flat, it is thought that the decrease in size and number of parts, as well as the simplicity of assembly and less critical tolerances make it a more practical unit.

In adjusting the regulators, both with the nested cantilever and Belleville springs, it was found that the shape of the regulator adjustment characteristic was appreciably affected by the proper location of the spring armature unit with respect to the magnet face, that is, the air gap for zero spring force. On the graphi-

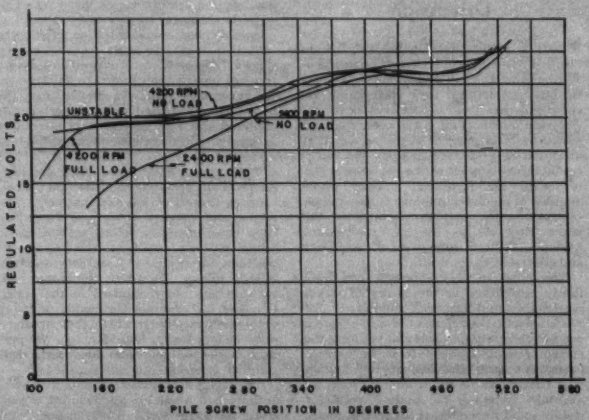


Figure 12. Performance characteristic of regulator with Belleville-type spring

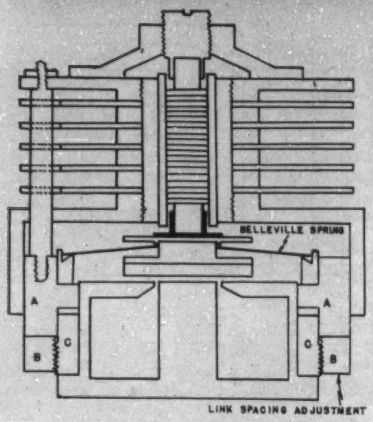


Figure 13. Sectional view of regulator with Belleville-type spring, illustrating the "link spacing adjustment"

cal representation of the magnet and spring curves (see Figure 7), this corresponds to the displacement between the origins of the magnet and spring curves. It is obvious that this displacement is a critical dimension, since no two magnet curves have exactly the same shape and the spring is designed to match one specific magnet curve. Therefore, it is necessary to be able to adjust the dimension of the mechanical linkage between magnet and spring until the spacing gives the proper displacement between origins. An adjustment was built into the regulator when the Belleville springs were used.

Figure 13 shows a sectional view of the regulator, in which the parts providing the above adjustment have been marked *A*, *B*, and *C*, and called the "link spring adjustment."

In the redesigned regulator, the rheostat in series with the magnet coil is still of value, since this controls the over-all resistance of the magnet circuit, and therefore acts as a voltage adjustment without affecting the shape of the magnet curves. The magnet core adjustment is not necessary.

The effect of adjusting the magnet core is to change the linkage paths in the magnet and therefore the shape of the magnet force versus air gap curves. It is possible that this adjustment could compensate for some variations in spring characteristics, but with well-designed springs the "link spring adjustment" gives better results.

Conclusions

1. The operating characteristics of the present carbon pile voltage regulators have been presented and analysed.

2. A graphical method of predicting performance from the static characteristic curves has been developed with the aid of the mathematics.

3. By application of the methods of analysis it was determined that the present regulators can be improved by changing the spring design. This conclusion is valid for most aircraft applications where hunting due to the external circuit inductance is normally absent. Two regulators with improved springs are described in the paper.

4. In a properly designed regulator the magnet core adjustment is unnecessary, but an added adjustment to control the link spacing was found to result in further improvement in the regulator characteristics.

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Discussion

Robert L. Mills and **Donald G. Scorgie** (United States Naval Research Laboratory, Washington, D. C.): The authors have particularly emphasized their chief initial assumption that "the characteristics of the electrical circuit (that is the generator and associated load) may be neglected and that attention may be focused upon the force-equilibrium equation of the mechanical elements of the regulator." Their whole analysis of regulator stability is based upon this assumption, and yet throughout the paper they always use as experimental evidence a system including both regulator and generator.

The error in this approach has been firmly established in the theory of servomechanisms and feed-back amplifiers. For it is known that one element in such a system can be inherently unstable and yet have the system remain stable. It is further established that all the elements in a system may themselves be stable and yet form an unstable system. To analyze regulator stability and then offer, as supporting proof, the performance of a regulator-generator combination, simply has no meaning. This objection can only be proved to be valid, of course, if the system in question satisfies the criteria necessary to apply servomechanism theory. At the Naval Research Laboratory this has indeed been shown to be the case; for reasonably large departures from the initial operating point both the carbon pile regulator and a d-c generator act essentially as linear amplifying elements in a closed loop system.

The authors have made static measurements of the force versus deflection characteristics of the spring and magnet. The spring and magnet deflections for which they experimentally found system instability are not totally unrelated to the positions for regulator instability specified in their theory. However, the proper relationship was not recognized in the paper. Consider for example the region of rising voltage characteristic which they obtained upon adding load. Consider, too, the portion of the stable region for which their stability criterion was not satisfied.

The experimental stability data which the authors had available all included a generator of some sort. The regulator and generator can both be considered as linear amplifiers in a single loop feed-back control system. As such each has a characteristic transfer function and amplification factor. When the amplification of the regulator grows large, the feed-back loop amplification becomes correspondingly large and the likelihood of satisfying Nyquist's stability criterion decreases. It has been shown that the carbon pile regulator has a monotonically increasing phase lag with increasing frequency. Furthermore, there is always some frequency for which its phase lag grows greater than 180 degrees. In addition, the phase lag of the generator increases with frequency. Therefore, there is always some finite amplification of the loop for which the system will become self-oscillatory no matter how infinitesimally small is the input disturbance.

How is this fact related to the authors' regulator stability criterion? The important slopes to consider are the magnet curve and the carbon pile curve rather than the magnet curve and spring curve as considered by the authors. When the carbon pile and magnet curves are parallel the amplification of the regulator is infinite. That is to say, an infinitesimal change of applied voltage gives rise to an infinite change of carbon pile re-

sistance. The feed-back loop amplification is then infinite and the system must surely oscillate as pointed out before. Fortuitously the carbon pile and spring curves are nearly parallel in the particular regulator analyzed by the authors. Therefore, the criterion they proposed is fairly good in accounting for the observed instability in this case.

The redesign of the spring which the authors undertook prevents the possibility of infinite amplification in the feed-back loop. This does not necessarily preclude the possibility of self oscillation of the system if the generator amplification is large, or if the time constants of the system are changed. The experimental work of the authors was done with a single generator, while it is well known from experience that a regulator which performs well on one system might be entirely unsatisfactory when in combination with a different generator.

The authors' answer to the stability problem has been an extremely critical change in the design of the regulator spring. In the past, mass production of these springs has proved to be an extraordinarily tricky task. It was by no means demonstrated in the paper that the trial-and-error cutting of the spring was practical on a mass production scale.

It seems to us that proposing critical changes in an already critical process is a basically wrong approach. The whole system should be analyzed, critical things made less so, and improvements made on those elements where more easily controlled changes can be accomplished.

W. B. Kouwenhoven and **G. J. Thaler:** The discussion presented by Mills and Scorgie is very interesting. Some of the factors mentioned by them deserve more consideration than has been given them. The authors wish to point out, however, that they were assigned the task of improving the performance of a given regulator with specific instructions that only a minimum number of changes should be made; also, the only generator available for testing the regulators had a very slow response. These limitations made it impossible to investigate many interesting possibilities.

The authors are not in complete agreement with certain specific statements in Mills and Scorgie's discussion. These are quoted and answered below:

1. . . . "To analyze regulator stability and then offer, as supporting proof, the performance of a regulator-generator combination, simply has no meaning."

The analysis was performed on a single regulator-generator combination in which the generator effects were obviously negligible. The resulting analysis certainly explains the performance of that specific unit and as such definitely has some meaning. It is true that generalization from such an analysis requires great care, but the discussion implies that the analysis will not permit generalization, and proof of this implication is lacking. On the contrary, the Westinghouse Electric Corporation has incorporated in their new regulator a coned spring with characteristics similar to our Belleville spring, and also have included a link-spacing adjustment. This seems to indicate that some of the results obtained from the analysis are applicable to other regulator-generator combinations.

2. . . . "However, the proper relationship (between magnet and spring deflections) was not recognized in the paper. Consider for example the region of rising voltage characteristic which they obtained upon adding load. Consider, too, the portion of the stable region for which their stability criterion was not satisfied."

The discussion fails to point out what is wrong and does not state what the proper relationship is. Mills and Scorgie then refer to two examples which are supposed to indicate a contradiction between the stated relationship and experimental results. They say first that a rising voltage characteristic is obtained upon adding load. If we refer to Figure 12 of the paper, which is the adjustment curve for the redesigned regulator using the Belleville spring, it is seen that the voltage at full load is always less than the voltage at no load (for the same speed) except at the end of the adjustment range where the magnet armature is pressing against the magnet yoke, and the unit is no longer operative as a regulator. The authors did not find a rising voltage characteristic upon adding load. The second condition referred to is "the portion of the stable range for which their stability criterion was not satisfied." Just what is referred to here is not clear, but the authors assume that it is the unstable region in the 4,200 rpm no-load curve Figure 12. This is not in contradiction with the stability criterion because, as specifically stated in the paper, the spring design was intended to approximate the magnet curve, and an exact match was not obtained. Perfect performance was not expected. The instability noted was due to a defect in the spring characteristic, not in the stability criterion.

3. . . . "Fortuitously the carbon pile and spring curves are nearly parallel in the particular regulator analyzed by the authors. Therefore the criterion they proposed is fairly good in accounting for the observed instability in this case."

It was perhaps fortunate that the authors worked with a regulator-generator combination in which certain effects were negligible. However, the implication in item 3, and also in item 2, is that the results obtained, and particularly the stability criterion have no value for other combinations. This is not so. Any regulator-generator combination should be stable when the generator load and speed are constant, and no matter what generator is used with the regulator the combination will not be stable unless

$$\frac{dF_s}{dx} > \frac{dF_m}{dx} + \frac{dF_{cp}}{dx}$$

because if this equation is not satisfied no restoring force is set up when the armature of the magnet is displaced, and, therefore, any transient mechanical shock will produce instability.

It is true, of course, that a regulator which satisfies this equation may still be unstable under dynamic conditions when used with certain generators. It then becomes important to know whether stability can be obtained by adjusting the degree of inequality in the equation, or by added damping, or by limiting the permissible compounding in the generator, or by some more radical change. These matters were not studied by the authors.

4. . . . "The important slopes to consider are the magnet curve and the carbon pile curve rather than the magnet curve and the spring curve as considered by the authors."

This fact was recognized by the authors. It is implicit in the equations used. It is apparent in Figure 7 of the paper that the important relationship involves the magnet and the carbon pile curves. Neglect of the carbon pile curve in later discussion was deliberate and the reasons for doing so were specified.

5. . . . "The authors' answer to the stability problem has been an extremely critical change in the design of the regulator spring."

Actually the authors' answer to the stability problem was to satisfy a given equation and at the same time to make the magnet and spring curves parallel. This was specifically stated in the paper. The redesign of the spring to accomplish this more general requirement was dictated by the necessity of making the minimum possible number of changes.

Sensitive Relay Contact Protection Systems

J. P. DALLAS
MEMBER AIEE

T. H. McCULLY
NONMEMBER AIEE

Synopsis: A protective network for sensitive relay contacts can be expected to extend reliable operation to several million cycles of operation. This result is to be achieved only if the sensitive contact circuit current is limited. Peak current transients on closure are shown to be the critical factor rather than the system voltage as popularly supposed. A 120 volt d-c sensitive contact circuit is considered safer and easier to protect than a comparable 24-volt circuit. It is shown that inductive loads can be successfully protected to values of almost two times that permissible for a resistive load with the type of protective system under consideration. Design procedure is given for a 2-circuit polarized relay protection system estimated to weigh less than eight ounces. It is suggested that available polarized relays when used with the subject protective system will provide power sensitivity, accuracy, and reliability in excess of that available from comparable electronic amplifier equipment.

MANY aircraft electric equipment design problems when reduced to their simplest terms result in contact-making devices. Remote positioning; automatic temperature, pressure, and flow control systems; voltage regulation; and reverse current, over voltage, under voltage, and fault detection devices; often reach their ultimate simplification through the use of a simple contact making armature mechanically or electromagnetically coupled to the function to be controlled.

The field of application of such devices has been greatly broadened by recent technical progress. Problems of stability and the elimination of hunting in contact-making controls can be solved by the use of simple feed-back circuits for introducing velocity correction factors into the control relay circuit. Modulating control action accompanied by substantial increases in sensitivity and accuracy is now commonly obtainable in contact-making control mechanisms through pulsing techniques. Improvements in magnetic and contact materials and in the design of relay mechanisms have been responsible for a recent substantial increase in the sensitivity and accuracy of such devices. Sensitive relays designed to meet aircraft requirements for resistance to acceleration and vibration with sensitivities below one-half milliwatt are commercially available.

Lest our enthusiasm for sensitive contact making controls appears to exceed the bounds of good judgment and common experience we hasten to state that such devices in their present stage of development used without adequate contact protection systems are not suitable for many aircraft applications because of a serious lack of reliability. As a result of failures due to neglect of contact protection there has developed a consistent trend away from contact making devices in aircraft design. Voltage regulators have changed from contact making types to the carbon pile. Nearly all recent servo-mechanical developments have adopted the electronic mode of operation. In the past ten years almost every major aircraft electric equipment manufacturer has experimented with and abandoned contact making controls in favor of heavier and more complicated, but more reliable devices. It is the purpose of this paper to suggest that this may have come about largely as the result of a failure to investigate sufficiently contact making protection systems.

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J. P. Dallas and T. H. McCully are with the Hughes Aircraft Company, Culver City, Calif.

Causes of Relay Unreliability

If we assume adequate protection from environmental causes of failure by hermetic processing, then sensitive contact failures in operating inductive circuits occur for the following reasons:

Destruction of the Contacts Due to Arcing. This is usually the result of a gross misapplication or overrating of the relay and deserves very little comment. In general if there is visible sparking at the contacts, failure will occur in a few thousand cycles.

Metal Transfer. In an unprotected inductive circuit the energy stored in the inductance is dissipated in the contact gap when it opens. This results in erosion of the contact surfaces and the building up of a pointed protrusion on the surface of the negative contact. It can readily be seen that in dealing with gaps of the order of 0.005 of an inch, very little metal transfer could be tolerated.

Sticking or Welding of Contacts. Where reasonable caution has been used in the design this is the most common cause of failure. It is also the most unpredictable. Contacts may develop a tendency to stick at irregular intervals of many thousands of cycles of operation. The genesis of this type of failure as shown in Figure 1 is usually as follows: (1) through contact erosion and metal transfer the effective contact area of the surface is so reduced that the current density is increased to the point where welding of the two contact surfaces occurs, (2) since the contact separating forces are extremely low, microscopic welds can produce sticking. Subsequent breaking of the weld with increased signal is likely to again alter the surface contact area so that normal operation of the contacts will be resumed until a new minute protrusion of the negative contact again reduces the contact area, increasing the current density to where a weld again occurs.

Because this type of failure is dependent upon unpredictable changes in the contact surface, which vary with metallurgical imperfections in the contacts, mechanical wear, and electrical transients in the circuit; it is almost impossible to predict sensitive contact reliability in terms of ordinary testing procedure.

Unless contact erosion is effectively controlled, microscopic welds of the con-

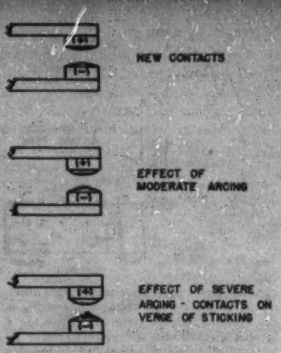


Figure 1. Contact erosion and closure by action of d-c arc

tact surfaces requiring a measurable separation force can occur at almost any assignable current value. If we specify our maximum contact separating force at one gram, then experience shows that currents up to approximately 50 mils can be reliably handled as long as the contact erosion due to energy dissipation of the arc at the break is held to the minimum made possible by present contact protection systems, which come very close to entirely eliminating the arc at break.

Since the peak current upon contact closure is the critical factor, it is desirable to employ at least 120 volts in the contact circuit. The sensitive relay is usually called upon to operate a contactor coil having power requirements which are the same regardless of circuit voltage. It is obvious, therefore, that in order to hold the load current to a minimum, the supply voltage must be as high as practicably possible. For example, a currently available contactor with a 120-volt coil has 3,000 ohms resistance, which gives us a coil current of 40 milliamperes. This is well within the desirable maximum of 50 mills. This same relay with a coil wound for 28-volt operation would have a resistance of 160 ohms and a coil current of 170 mills, making the protection problem very difficult. Since the ampere turns requirement of a given relay does not change with coil voltage it follows that the stored energy will also be constant.

There is one other factor that makes operation at high voltages desirable. The load impedance for the same power requirement is much higher than for a low supply voltage. The effectiveness of a contact protection network is determined by achieving a small ratio of the contact protection impedance to the load impedance. This is much more readily achieved when the load impedance is high.

Contact Protection Systems

The object of contact protection systems is to provide a means for absorbing the energy normally dissipated in the contact gap when the circuit is open. In the simple resistive circuit of Figure 2, the voltage rise is almost instantaneous. An arc begins at point A and ends at point B. The arc duration will be of the order of one millisecond. It is to be noted that the jagged peaks on closing are the result of contact bounce. While good relay design should be aimed at eliminating contact bounce, it is not always possible to do so. For this reason, contact protection systems must be designed to take care of circuit transients resulting therefrom, and must be designed around the characteristics of the particular relay to be used.

The addition of a capacitor across the contact as shown in Figure 3 is the first step in contact protection. It will be noted that the entire energy of the contact opening is absorbed in the capacitor eliminating all evidence of arcing. When the contacts are closed the capacitor is discharged and a very high current peak is produced. For example, with a 40-milliampere 120-volt d-c contactor coil load all evidence of arcing at the break will disappear when the value of the capacitor is approximately a tenth of a microfarad. But the current peak through the contacts when they are closed may reach a value of several amperes which would be sure to weld the contacts.

In order to limit the peak current through the contacts to some safe value it is necessary to add a resistor in series with the capacitor as in Figure 4. Since the voltage appearing across the capacitor when the circuit is open is the full line voltage it becomes obvious that the series resistor r can be selected by Ohm's law to limit the peak discharge current to any desired value.

Contrary to general belief, contact protection for sensitive relay circuits is more difficult for purely resistive circuits than for inductive circuits. This is because the peak current appearing at the contacts when the circuit is closed as at A in Figure 4, is the sum of the normal resistive load current plus the discharge current from capacitor C through resistor r. It is obvious that if an inductive load were employed the instantaneous peak on closing would be much lower. The inductance would delay the rate of rise of the normal circuit current so that the instantaneous peak current would be substantially that determined by capacitor C discharging through resistor r.

For example, if the capacitor series resistor r of Figure 4 is gradually increased at some point close to the resistance of the load, the effectiveness of capacitor C in preventing an arc will diminish to the point where a visible arc will appear. If r is made approximately 0.8 of the load resistance R the best compromise between contact breaking arc suppression and circuit closing peak current will be affected. In the previous example, the normal load current would be 40 milliamperes and the peak current supplied by the capacitor discharge would be 50 milliamperes, so the total peak current at point A when the contacts are closed would be 90 milliamperes.

Referring to Figure 5, if exactly the same circuit constants are used but with the additional load inductance L, the time constant of this inductance will delay the

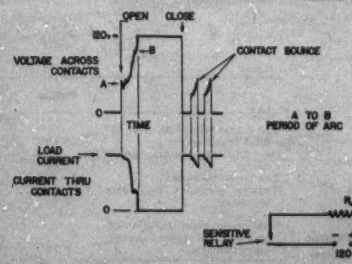
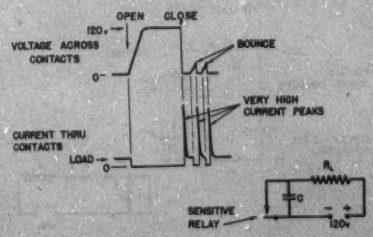


Figure 2 (left). A resistive circuit without contact protection

Figure 3 (right). A resistive circuit with simple capacitor contact protection



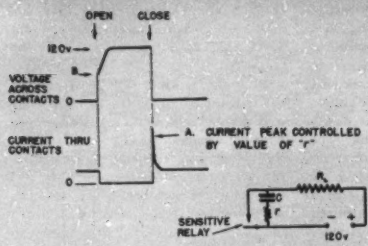


Figure 4 (left). A resistive circuit with R-C contact protection

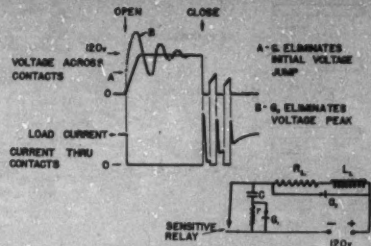


Figure 6. An inductive circuit with R-C and germanium rectifier contact protection

rise of the normal circuit current of 40 milliamperes so that the peak current of the capacitor discharge does not add to the normal load current. The peak current transient at closing for Figure 5 will therefore be approximately 50 milliamperes instead of the 90 milliamperes which would occur with the pure resistance load.

Referring to Figure 6, germanium crystal diode rectifiers have been added across the load and across the R-C network resistor. The diode connected across the load dissipates the bulk of the energy stored in the load, effectively removing the peak voltage at the opening of the contacts. This has the effect of reducing an inductive circuit to a resistive circuit as far as the circuit performance at the instant of contact separation is concerned. This will have no effect upon circuit conditions at the instant of contact closure. The rectifier placed in parallel with resistor r in Figure 6 reduces the impedance of the protective network at the instant of contact separation. Therefore, the rise of voltage across the contacts at the instant of contact separation is greatly reduced below that experienced by the basic R-C protective network.

Design Procedure

Because of the many indeterminate variables inherent in a contact system, an empirical design procedure based on oscilloscope observation rather than mathematical analysis is the most practical way to design sensitive contact protective systems at present.

The basic design philosophy of any protective system intended to increase reliability must recognize that every added device carries with it inherent liabilities of failure as well as possible protective advantages and that where a protective system of several elements is proposed the separate liabilities of these elements must not add, or there will be little real gain in reliability. The design of a protective system should be aimed at securing the maximum of protection from the remaining elements of the system after the assumed safe failure of any one element.

With these ends in view the design of the system illustrated in Figure 6 should be undertaken in the following order:

1. The capacitor series resistor r in Figure 6 should be as high as possible without producing appreciable arcing on contact opening as determined by oscilloscope observation. A practical value of r for sensitive relay contact circuits not exceeding 60 milliamperes is approximately 0.8 of the load resistance. This resistance determines the peak discharge current through the contacts on closing by a simple Ohm's law relation.

2. The contact shunt capacitor value is determined by oscilloscope observation of the voltage and arc condition across the contacts and the peak currents observed in the contact circuit on closing. The equipment for making these observations is described in the Appendix and Figure 7. The capacitance is increased from zero to a minimum value that will eliminate contact arcing with r equal to about 0.8 of the load resistance. The capacitance required will be of the order of 0.01 microfarad. By increasing the capacitance above this minimum value the peak voltages appearing in the system may be reduced. Thus, in a typical circuit of the type discussed an increase of C to 0.2 microfarad reduced a peak voltage of 800 volts to 400 volts. A safety factor of two to three on the voltage rating of the capacitor is recommended.

It is to be noted that optimum adjustments for minimum peak current during contact bounce will not correspond to the adjustment for minimum peak voltage on contact separation, and the optimum value taking into account both conditions should be chosen. The primary reason for taking so much care with the design of the R-C contact protection network is to insure that it will have maximum effectiveness should the rectifier portions of the circuit fail or become inoperative. Since the best of rectifiers often leave something to be desired in the way of reliability, this is a necessary precaution.

3. The application of the rectifiers to the basic R-C protective circuit is as follows. The current passing through the rectifiers cannot exceed the normal load current because of the nature of the inductive property of the load. This is readily understood by pointing out that the protective network constitutes a bypass circuit around the contacts through which the normal current is rerouted at the instant of contact separation. It is the property of the reactance of the load to behave so as to maintain this flow of current. Various types of rectifiers have been investigated for this application, and the germanium crystal diode was found to be by far the most satisfactory for two reasons. It had very low capacity between its terminals, and therefore did not contribute to the peak discharge current through the contacts at the instant of closing. It is the only available rectifier that has a back to forward ratio exceeding one thousand.

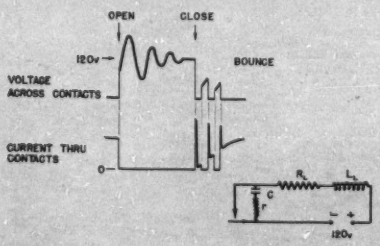


Figure 5. An inductive circuit with R-C contact protection

Table I

	Ounces
Two 0.2-microfarad 600-volt oil-filled capacitors	3.00
Four *1N39* germanium crystal diodes	.57
Two 1-watt resistors	.25
Hermetic enclosure and plug mounting	3.56
Total weight, hermetically-sealed plug-mounted dual contact protection unit	7.38

The germanium rectifier that is most suitable for this application is known as the *IN39* crystal diode. Its maximum allowable inverse voltage is set at 200 volts direct current and under normal circumstances as applied in this circuit, the inverse voltage across either of the rectifiers will not exceed the supply voltage. The *IN39* has a rated peak anode current of 150 milliamperes.

If the foregoing procedure is followed, a protective network for the sensitive relay contacts will result which can be expected to extend reliable operation to several million cycles of operation. With available components the weight of a network system for a polarized relay with two contact circuits is shown in Table I.

Conclusions

A self-contained contact protection unit hermetically sealed which can be expected to extend the reliable operation of a sensitive polarized relay to several million cycles of operation can be constructed from available components at a weight of less than 8 ounces.

The critical design factor for a sensitive relay contact protection network is the contact circuit current. For present commercial sensitive relays 50 milliamperes is the desirable maximum.

It can be shown that with a given load power requirement, raising the voltage of the contact system will not change the amount of energy stored in the load which must be dissipated in the contact gap or absorbed by the protection system. This is true because the energy stored is proportional to the square of the ampere turns which is constant for a given contactor regardless of the supply voltage of the circuit. A protected 120-volt d-c sensitive contact circuit can safely handle more power than an equivalent 24-volt circuit. The effectiveness of a contact protection network is determined by achieving a small ratio between contact protection impedance and the load impedance. This is much more easily achieved in high-voltage control circuits.

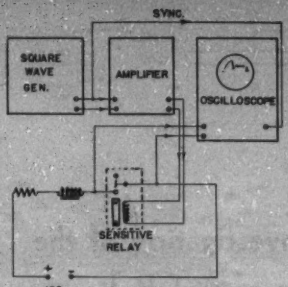


Figure 7. Test equipment circuit

Inductive load circuits are easier to protect than resistive load circuits, and in general inductive loads can successfully be protected to values of two times that permissible for a resistive load. This is because current is the critical factor and the time delay in the buildup of the current in inductive circuits prevents the additive peaking of load and protective network discharge current.

Severe contact erosion through metal transfer to the negative contact will reduce contact gaps instead of increasing them because the deposited metal taken from a considerable area of the positive contact is deposited on the negative contact in the form of a protrusion. The contact area is diminished until current densities reach the welding value. For any given current this will occur at some physical dimension for the weld which will require a definite maximum separating force.

Appendix

The equipment used for measuring voltage and current transients in the contact system and for measuring the circuit parameters necessary for the design of the contact protection system, is standard laboratory equipment. Referring to Figure 7; in order to use a standard oscilloscope such as the Dumont *Model 241* which does not have a provision for satisfactory transient observation, it is necessary to provide for repetition of contact opening phenomena at some convenient sweep rate. To accomplish this, the sensitive relay under observation was driven by a square wave signal of approximately three volts, and at a frequency of about ten cycles per second. This method of observing contact circuit transients has the advantage of presenting to the eye or camera a picture of maximum transient values for a large number of cycles of operation.

Contact circuit voltage transients were applied directly to the vertical deflection plates of the scope. Contact circuit current transients were picked up from across a 10-ohm series resistor and applied to the vertical amplifier.

No Discussion

An Experimental Investigation of the Electrical Performance of Bolted Aluminum-to-Copper Connections

W. F. BONWITT
NONMEMBER AIEE

Synopsis: Bolted bus connections between bare aluminum and copper, with an electrotinned and flowed finish, will provide a low-resistance joint which will be reasonably stable even under extreme conditions of elevated temperature and corrosive attack. While the use of a compound, Penetrox "A," will greatly improve and stabilize the contact conditions, the combination of aluminum and copper (electrotinned and flowed), will form a satisfactory electric connection even when made without the use of any compound.

THE increased use of aluminum as an electrical conductor has raised the problem of suitable connectors, especially when connections have to be made between aluminum and copper. Since aluminum-to-aluminum connections offer no serious problems which have not been satisfactorily solved in one way or another, this investigation has been confined to the electrical performance of aluminum-to-copper connections.

The purposes of conducting this rather extensive investigation were to determine:

1. The feasibility of making direct connections between aluminum and copper.
2. The most suitable finish (plating) for the copper member of the connection (the aluminum member was always bare).
3. The effect of using a compound in such connections.

Obviously, the problem of making aluminum-to-copper connections is not confined to bolted bus-bar connections, such as were used for the experiments described in this paper. The problem of connecting aluminum conductors in the form of wire or cable to copper conductors of similar form, or to copper bus bars, is encountered even more frequently, and in this case, the connection is rarely a direct one, but requires the use of an intermediate member, a connector. The question then is: Should the connector be made from aluminum, or should it be made from copper or a copper alloy? And in the latter case, should such a connector be plated, and if so, which is the best finish?

It is easy to understand that the ideal solution would be a universal connector, which will work equally well with copper and aluminum. This has been recognized for a long time, and many attempts have been made to provide such a connector. At present, several copper, or copper alloy connectors are being offered for use with aluminum conductors. All these connectors have a finish (plating) on the copper base metal, and in addition, some of the manufacturers specify the use of a compound in making these connections between the aluminum conductor and the connector.

Since the practical experience with these connectors is as yet limited, it was found desirable to determine the most suitable finish or plating for the copper member of the connections, and the effect of different compounds on the electrical quality of these connections.

Experimental Data

In order to eliminate the unavoidable variables which are introduced when using actual aluminum cable and the proper connector, the experiments were carried out with bolted bus-bar connections between aluminum and copper, and the test procedure consisted of exposure of the test samples to elevated temperatures and to a prolonged salt-spray test.

Although the tests were made with bolted, direct connections of aluminum and copper bus bars, it is reasonably safe to apply the findings to bolted or clamped connections for other than flat conductors. The problems created by the use of aluminum in contact with copper are essentially the same, whether flat bar is concerned, or whether a stranded or solid round conductor is clamped in a connector: the galvanic dissimilarity of the two metals with its possible serious effect in the case of corrosion; and the comparative softness and ductility of the aluminum as compared to copper, with the possibility of cold flow, relaxation, and consequent reduction of contact pressure.

The test conditions were selected with the foregoing in mind. The heat treatment was thought to accelerate possible relaxation by repeated expansion and contraction of the connection, in addition to promoting possible formation of high-resistance oxides on the contact surfaces. The extended salt-spray test procedure, on the other hand, would provide extremely severe corrosive conditions. In addition, an extended, natural, ageing period, prior to the two test procedures, was included.

Separate test samples were used for the two test procedures. All test samples however were identical in respect to the design, material used, and their dimensions. Figure 1 gives a view of the test sample, and also the significant dimensions. Aluminum, as well as copper bars, were cut from mill lengths of bus bar, 1 by 1/4 inches. The aluminum (center) bar was always bare, while the various finishes were applied to the two end bars which were made from copper. Since the dimensions of the samples, as well as the

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W. F. Bonwitt is with the Burndy Engineering Company, New York, N. Y.

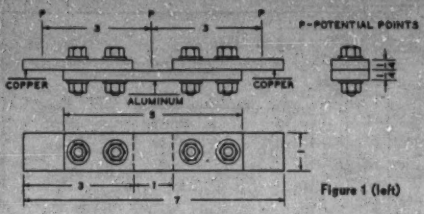

Figure 1 (left)

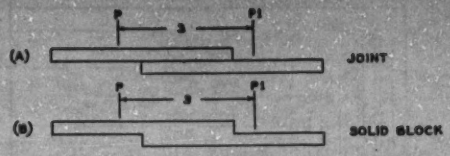

Figure 3 (right)

(Bolts omitted)

hardware used, were alike, and since care was taken to apply the same tightening torque to all the samples, the contact pressure can be assumed to have been identical for all samples.

A tightening torque of 250 inch-pounds was applied to the bolts. Tests had shown that this torque would provide a contact pressure of approximately 3,000 pounds per square inch. This may seem an unusually high contact pressure, but since this determination was made immediately after assembly, and since the relaxation, especially in the case of the aluminum bar, is considerable, this value probably decreased rather rapidly in the immediate period following assembly.

The bolts and nuts, as well as the washers, were steel, cadmium-plated with the commercial thicknesses applied to such hardware. Bolts were 3/8, 16, $1^1/_4$ inches long. Washers were placed under the boltheads and nuts to provide a better distribution of the pressure, and while this may not be standard practice, it seems advisable in the case of aluminum connections.

There were seven variations of the copper bars. They were selected with a view of including the different finishes or coatings now being used on connectors for aluminum conductors, mainly cadmium and tin platings. A number of variations of these two basic platings were tested also. As a control, bare copper bars were included, and zinc-plated copper bars were added.

Following is a list of the various sample groups. The numbers given for each such group correspond to the actual numbers of the assemblies (see the test data record in the appendix, Tables V to XI). The odd numbers referring to samples tested at elevated temperatures, and the even numbers referring to the samples in the corrosion tests.

1. Numbers 1–6, bare copper, bright-dipped before installation.
2. Numbers 7–12, copper, cadmium-plated, 0.001 inch.
3. Numbers 13–18 copper, cadmium-plated, 0.0003 inch.
4. Numbers 19–24, copper, electrotinned, 0.0003 inch.
5. Numbers 25–30, copper, electrotinned, 0.0003 inch, and flowed.
6. Numbers 37–42, copper, hot tin-dipped.
7. Numbers 43–48, copper, zinc-plated, 0.0002 inch.

A few remarks regarding the finishes (platings) just listed shall be made here.

As mentioned before, the center aluminum bar was always bare, and no cleaning process was applied to this aluminum bar before assembly, except for a wiping with carbon tetrachloride to remove any possible dirt and grease. While it is realized that careful cleaning is frequently applied to aluminum before installation, and, as a matter of fact, should always be done, it was the intention from the outset to conduct the experiments under the most adverse conditions. Furthermore, while cleaning of flat bar is a simple operation, the cleaning of wires or cables is a different matter, and it is safe to assume that a large portion of the conductors are not cleaned prior to installation beyond the scraping action applied in removing the cable insulation.

The various copper bars were wiped also with carbon tetrachloride before assembly to remove dirt and grease. As far as the surface of the bars is concerned, and that is true for aluminum, as well as for copper bars, no particular finish was attempted. All bars were cold rolled, and their surface was in no way improved before use. Only around the drilled holes were the edges chamfered to insure good contact between the two bars.

Cadmium plating was tested in two thickness, 0.0003 inch and 0.001 inch because of the sometimes encountered opinion to the effect that this particular metal was only effective when applied in comparatively heavy layers.

As can be seen from the list, electrotin-plating was tested in two variations: as a regular electrotin deposit 0.0003 inch thick; and in flowed condition. The flowing process, which turned out to produce the best finish tested, was accomplished by plating the regular thickness of tin, and then immersing the plated sample into hot palm oil. The result is a conversion of the crystalline structure of the plating into an amorphous structure, this conversion resulting in a smooth, shiny surface.

The hot-tin dipping was done by dipping the samples into a 35-65 alloy of lead and tin. The thickness of the coating was not measured, but was, of course, much greater than that of any of the electro-deposited platings.

The zinc plating was tested since this metal was one of the first ones to be used in connection with aluminum, and zinc-plated aluminum connectors had shown good results in contact with copper.

Each of the seven variations listed was tested in three different manners, without a compound, with Penetrox "A" compound, and with a third compound, described subsequently and designated as "compound B."

In selecting these three variations, it was taken into consideration that a compound is more or less generally recommended for aluminum connections, but it was felt that any finish of the copper members of the connection, in order to be completely acceptable, should be satisfactory even if no compound was applied. It will be realized, that no matter how strongly the use of a given compound is recommended, there is no assurance that

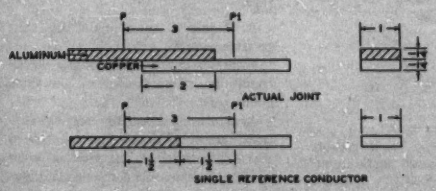

Figure 2

(Bolts omitted)

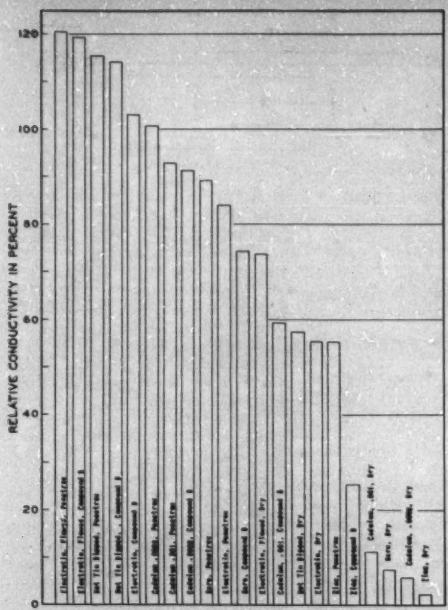

Figure 4 (left). Comparative standing of samples after elevated temperature test

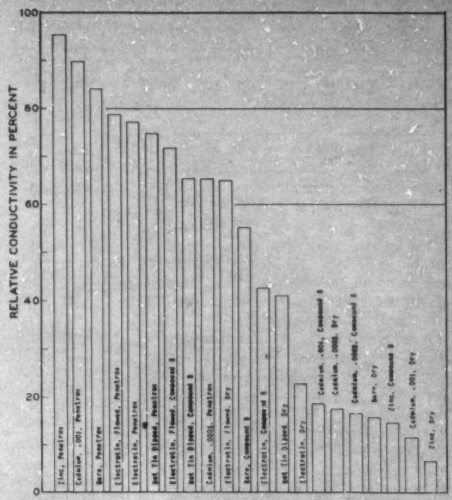

Figure 5. Comparison of samples after exposure to corrosive atmosphere

such a compound is actually applied before installation. This was the reason for testing samples without a compound.

Penetrox "A"[1] is a petrolatum-zinc compound and is recommended for all aluminum connections. Its functions are twofold: its metallic components penetrate the surface film of the aluminum (oxide), while the petrolatum acts as a protective coating to keep out moisture and to prevent oxidation of the contact surfaces.

Since it was realized that Penetrox "A" will not be used exclusively, "compound B" was tested also. This compound has been widely used for many years as an anticorrosive protection for electric connections, and is also a petrolatum base compound.

There are other compounds made and recommended for use with aluminum, but none of them were considered sufficiently tested for inclusion in this investigation.

Both compounds were applied in a uniform manner by coating (wiping) the contact surfaces of the aluminum and copper bars. No scratch brush or any other abrasive tool was used in this operation.

As mentioned briefly in the beginning of this paper, two test procedures were used: elevated temperatures; and salt spray. Identical but separate samples were used for the test procedures, and all samples were observed during a natural ageing period of 624 hours (26 days) after assembly, before undergoing either of the test procedures. The two test procedures are described subsequently.

1. Treatment at Elevated Temperatures

This test consisted of exposure of the test samples to various elevated temperatures for eight-hour periods. This was done for all samples simultaneously by placing them in a constant temperature air oven in such a way, that the bus bars would be supported in a horizontal plane. Such eight-hour exposures were made at:

Degrees Centigrade	(Degrees Fahrenheit)
75	(167)
100	(212)
125	(257)
150	(302)
175	(347)
200	(392)

These temperatures were kept within ±2 degrees centigrade, and resistance measurements were made on the samples after each such eight-hour run, after allowing the samples to cool at room temperature. The samples were placed in the oven at room temperature, and the oven temperature was raised to the respective values. This took from approximately 1/2 hour for the lowest test temperature, to approximately two hours for the highest test temperature, and the eight-hour period was started as soon as the test temperature was reached.

2. Corrosion Test Procedure

The second set of samples were used for the corrosion test which consisted of an extended exposure to a salt-spray atmosphere. This test was conducted for a total exposure of 1,000 hours, with resistance measurements made every 100 hours.

The samples were placed in a salt-spray chamber in which the temperature was maintained between 95 and 100 degrees Fahrenheit (35 and 37.8 degrees centigrade). The salt mist was generated from a 20 per cent sodium chloride aqueous solution by means of preheated air at a pressure of 15 pounds per square inch. This compressed air was saturated to avoid a so-called dry spray. The samples were supported in one horizontal plane so that the salt spray would not directly cover them.

It is apparent that both of these test procedures were extremely severe, and that they exceeded any reasonable service conditions encountered for aluminum connectors.

As mentioned repeatedly, only the electrical quality of the bus-bar connections was of interest in this investigation, as far as the performance of the connection was

Table I. Effect of Eleveted Temperatures

Comparative Standing of Samples

Order Number	Finish on Copper Bars and Compound Used	Joint Resistance, Microhms Final	Initial	Resistance Increase, Per Cent
1	Electrotin, flowed, Penetrox	9.0	10.0	10.0 (decrease)
2	Electrotin, flowed, compound B	9.1	9.3	2.2 (decrease)
3	Hot tin-dipped, Penetrox	9.4	10.0	0.6 (decrease)
4	Hot tin-dipped, compound B	9.5	10.1	5.9 (decrease)
5	Electrotin, compound B	10.5	10.1	4.0
6	Cadmium, 0.0003 inch, Penetrox	10.8	10.0	8.0
7	Cadmium, 0.001 inch, Penetrox	11.7	10.1	15.8
8	Cadmium, 0.0003 inch, compound B	11.9	39.3	59.7 (decrease)
9	Bare, Penetrox	12.2	9.4	29.8
10	Electrotin, Penetrox	12.9	9.5	35.8
11	Bare, compound B	14.6	13.0	12.3
12	Electrotin, flowed, dry	14.7	10.8	36.1
13	Cadmium, 0.001 inch, compound B	18.4	43.6	57.8 (decrease)
14	Hot tin-dipped, dry	19.0	12.2	55.7
15	Electrotin, dry	19.5	13.3	46.6
16	Zinc-plated, Penetrox	19.5	10.0	95.0
17	Zinc-plated, compound B	42.4	14.5	192.4
18	Cadmium, 0.001 inch, dry	97.1	37.3	160.3
19	Bare, dry	146.3	19.7	642.6
20	Cadmium, 0.0003 inch, dry	183.1	69.2	164.5
21	Zinc-plated, dry	490.8	28.2	1640.4

concerned. This electrical quality is best represented by the resistance from one bus bar to the other. Therefore, resistance measurements were made on each assembly, as shown in Figure 1, from the copper bar to the center aluminum bar on both sides, thus providing two measurements for each sample assembly. The measuring distance was uniformly 3 inches, this distance comprising a 2-inch overlap and 1/2 inch each on the copper and the aluminum bar. Measurements were made at room temperature by means of a laboratory precision Kelvin bridge, and always corrected for a 20 degree centigrade reference temperature so as to be directly comparable. The current used for these measurements was ten amperes, and this current was considered small enough to disregard any possible temperature rise of the samples due to the measuring current.

Results

The observations made on the samples during the test procedures are given in the Appendix, in Tables V to XI.

Since the criterion of performance is the resistance of the various connections, these resistances are tabulated in Tables I and II, dividing the data according to the two test procedures, elevated temperature, and corrosive atmosphere.

These two tables show the initial and final joint resistances of all samples where the figures given are the mean resistances of the two identical connections tested on the same assembly (see Figure 1). In order to provide the first rating of the samples, they are arranged in the order of increasing final resistance. Order numbers are assigned to each sample, and these order numbers appear also on Figures 4 and 5, which show the standing of the samples graphically. These graphs give the relative conductivities rather than the resistances, so as to show the absolute, as well as the relative standing, of the samples.

Since a finish on the copper should provide satisfactory contact conditions even if no compound was applied, the final resistances have been compiled in Tables III and IV for the seven finish variations each as a group regardless of the compound used by taking the average resistance of the three compound variations, for example, no compound, Penetrox "A," and "compound B." The groups have been arranged in the order of increasing final resistance.

Similar to the procedure followed in summarizing the sample resistance, a bargraph (Figure 6) shows the performance of the seven finish-groups expressed by the relative conductivity. This graph shows not only the average final group conductivity but also the initial conductivity, thus giving the change of conductivity as well.

Discussion of Results

It is the object of this investigation to determine the most suitable finish for copper members of copper-aluminum connections. Furthermore, the best finish should be acceptable with respect to the electrical qualities of the connection, even when used without any of the compounds.

Tables III and IV, as well as Figure 6, show the foregoing conditions clearly. Disregarding the use of a compound, and simply averaging the resistances of the three variations tested of each finish, we find the three groups using tin as a finish to be the best. This is true for both test conditions, elevated temperature, and corrosive atmosphere.

Within the tin group, the flowed tin finish is the best, topping the list of all finish variations. All of these evaluations are based on the lowest joint resistance after test, which is equivalent to lowest contact resistance and highest relative conductivity.

Turning to the performance of individual samples, where the effect of the compound shows up, we find, as the most striking result, that the application of a compound results in a lower initial as well as final joint resistance on all samples regardless of their finish. Of a total of 21 samples used in each test procedure, the first 11 samples tested at elevated temperatures used a compound. And under corrosive conditions, the first nine samples

Table II. Effect of Corrosive Atmosphere

Comparative Standing of Samples

Order Number	Finish on Copper Bars and Compound Used	Joint Resistance, Microhms Final	Initial	Resistance Increase, Per Cent
1	Zinc-plated, Penetrox	11.4	10.4	9.6
2	Cadmium, 0.001 inch, Penetrox	12.1	9.3	30.1
3	Bare, Penetrox	12.9	9.7	33.0
4	Electrotin, flowed, Penetrox	13.8	9.6	43.8
5	Electrotin, Penetrox	14.1	9.5	48.4
6	Hot tin-dipped, Penetrox	14.5	10.1	43.6
7	Electrotin, flowed, compound B	15.1	9.5	58.9
8	Hot tin-dipped, compound B	16.6	10.5	58.1
9	Cadmium, 0.0003 inch, Penetrox	16.6	10.3	61.2
10	Electrotin, flowed, dry	16.7	10.1	65.3
11	Bare, compound B	19.6	12.7	54.3
12	Electrotin, compound B	25.4	11.5	120.9
13	Hot tin-dipped, dry	26.4	10.8	144.4
14	Electrotin, dry	47.1	15.3	207.8
15	Cadmium, 0.001 inch, compound B	58.2	53.5	8.8
16	Cadmium, 0.0003 inch, dry	60.9	54.3	12.2
17	Cadmium, 0.0003 inch, compound B	64.3	47.7	47.1
18	Bare, dry	65.4	17.6	271.6
19	Zinc-plated, compound B	72.9	14.6	399.3
20	Cadmium, 0.001 inch, dry	95.2	55.2	72.5
21	Zinc-plated, dry	161.6	25.4	536.2

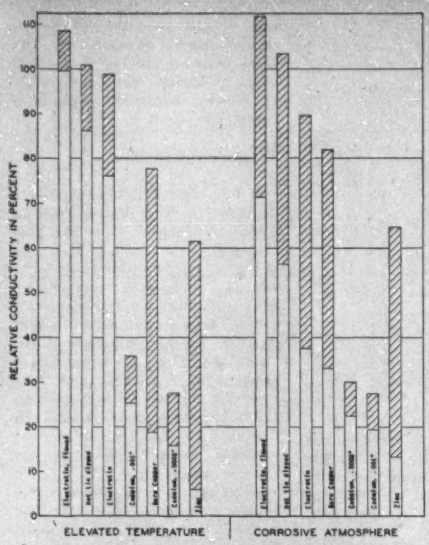

Figure 6. Effect of tests on sample groups

Shaded area indicates loss of relative conductivity due to test procedure

all had a compound applied. Distinguishing between the two used—Penetrox "A" and "compound B"—we see that Penetrox "A" is superior, especially under corrosive conditions (the first six samples had this compound). At elevated temperatures, there is little difference between the two compounds, as Table I shows.

While the preceding summarizes the comparative standing of the sample groups, the following covers the electrical quality of the connections as such. In other words, were the connections in all their variations good electrical connections, and how was their quality affected by the test procedures?

In order to discuss the quality of the connections, we first have to define what we consider to be a good connection. As stated at the beginning, we are only concerned with the electrical qualities of the connection, and we obviously want these connections to be good ones not only after installation but throughout the test procedures applied.

Since there is no generally accepted standard of a good electric connection, we will establish such a connection as one, which has an initial relative conductivity of at least 100 per cent (in other words, a connection with a resistance not greater than the resistance of a single conductor forming this connection). Furthermore, we do not want this initial resistance to increase more than about 50 per cent during any one of the test procedures, so that the final relative conductivity will not be lower than roughly 67 per cent. We have thus established two characteristics of a good electric connection, namely, its initial resistance level, (in relation to the resistance of the conductors forming this connection) and the permissible deterioration as a consequence of ageing (relaxation), and the two test procedures.

Using the preceding criteria as a yardstick, we find that the following samples meet the first condition, an initial relative conductivity of 100 per cent or better (in order of decreasing average):

1. Electrotin, 0.0003, Penetrox
2. Bare, Penetrox
3. Cadmium, 0.001, Penetrox
4. Electrotin, flowed, Penetrox, dry, "compound B"
5. Cadmium, 0.0003, Penetrox
6. Hot-tin dipped, Penetrox, "compound B"
7. Zinc-plated, Penetrox

It is interesting to note that this list contains all sample variations tested whenever Penetrox was used.

Applying the second condition of a relative conductivity of not less than 67 per cent at the conclusion of the test, the following samples meet this requirement, again in the order of decreasing average relative conductivity:

1. Electrotin, flowed, Penetrox, dry, "compound B"
2. Hot-tin dipped, Penetrox, "compound B"
3. Cadmium, 0.001, Penetrox
4. Bare, Penetrox
5. Cadmium, 0.0003, Penetrox
6. Electrotin, 0.0003, Penetrox

This second listing is identical with the

Table III. Average Initial and Final Group Resistances

Elevated Temperature

Order Number	Finish on Copper Bars	Joint Resistance, Microhms Final	Initial	Resistance Increase, Per Cent
1	Electrotin, flowed	10.9	10.0	9.0
2	Hot tin-dipped	12.6	10.8	16.7
3	Electrotin, 0.0003 inch	14.3	11.0	30.0
4	Cadmium, 0.001 inch	42.4	30.3	39.9
5	Bare copper	57.7	14.0	312.1
6	Cadmium, 0.0003 inch	68.6	39.5	73.7
7	Zinc, 0.0002 inch	184.2	17.6	946.6

Table IV. Average Initial and Final Group Resistances

Corrosive Atmosphere

Order Number	Finish on Copper Bars	Joint Resistance, Microhms Final	Initial	Resistance Increase, Per Cent
1	Electrotin, flowed	15.2	9.7	56.7
2	Hot tin-dipped	19.2	10.5	82.6
3	Electrotin, 0.0003 inch	28.9	12.1	138.8
4	Bare copper	32.6	13.3	145.1
5	Cadmium, 0.0003 inch	47.3	36.1	31.0
6	Cadmium, 0.001 inch	55.2	39.3	40.5
7	Zinc, 0.0002 inch	83.0	16.8	388.1

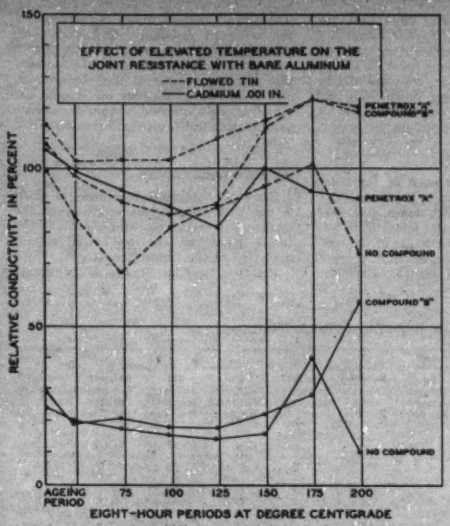

Figure 7. Flowed-tin copper versus cadmium-plated copper, effect of elevated temperature

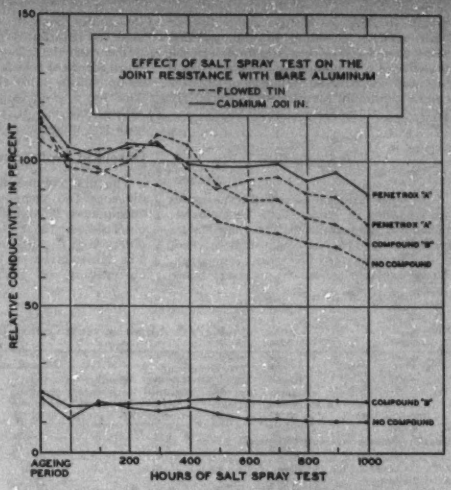

Figure 8. Flowed-tin copper versus cadmium-plated copper, effect of salt-spray test

first one, except that the zinc-plated samples dropped out because of failure in the elevated-temperature test. The failure of electroplated zinc in respect to its electrical properties at higher temperatures is well known.[2]

If Penetrox is used, all finishes tested (even no finish at all on the copper bar) will provide a satisfactory connection with the bare aluminum, with the single exception of zinc as a finish. However, we stated at the beginning of this paper that a finish in order to be fully acceptable as a coating for copper, should perform satisfactorily even with no compound applied at all. It can be seen from the two listings, that only a single finish met this condition, namely electrotin, flowed.

Since cadmium is used as a plating on copper, or copper alloy connectors to make them suitable for use with aluminum conductors, the performance of this metal as a finish on copper is of particular interest.

Figures 7 and 8 show the performance of cadmium-plated samples versus electrotinned and flowed samples, during the entire test as expressed by the relative conductivity of the connections.

As pointed out before, the application of Penetrox makes every finish satisfactory, and the cadmium-plated samples are no exception and they are as good as are samples with the flowed tin finish. However, the performance is strikingly different when samples without a compound, or with "compound B," are compared.

It is interesting to note that there is a definite trend of reversal of the relative conductivity as a result of the elevated temperature. This is particularly noticeable when "compound B" is used, although in general it applies to Penetrox also. This reversal seems to show that the contact resistance is reduced as higher temperatures are being applied.

There is another characteristic of cadmium-plated samples worth mentioning, that is, the high, initial resistance level of the samples, regardless of the thickness of the plating. When Penetrox "A" is applied, this resistance is reduced to a normal level, but in the case of samples without any compound and of samples with "compound B," the values are so high that the group resistances show this characteristic quite strikingly (Figure 6).

Since the resistance of cadmium is fairly low (lower than that of tin), and since it is quite unlikely that the metallic components of Penetrox "A" could penetrate a plating even 0.0003 inch thick, this behavior of the cadmium-plated samples can be explained only by the presence of a high-resistance surface film which is penetrated by the Penetrox "A" compound.

Conclusion

The results have in themselves shown that a satisfactory electric connection can be made between bare aluminum and copper. Furthermore, the results showed clearly that the electrical quality of such a connection can be maintained at an acceptable level through an extremely severe test procedure, both in respect to elevated temperatures and under corrosive conditions.

The following conclusions are based on the results given previously and also on the visual examination of the test samples as given in the Appendix:

1. The samples using tin as a finish for the copper member of the connections stood up better than samples with any of the other metals used, and were superior also to bare copper samples.

2. Within the sample groups using tin, the group with a flowed tin finish was the best.

3. The use of a compound between the contact surfaces will have a much greater effect on the level of the contact resistance and its stability than the finish on the metal.

4. Flowed tin was the only finish tested which produced satisfactory contact conditions throughout the test even when used without a compound.

5. Appearance of corroded samples and especially of their contact surfaces is misleading. A clean, bright surface does not necessarily indicate good electrical contact conditions (low-contact resistance).

6. The potential difference between two

metals forming an electric connection or their relative standing in the galvanic series should not be overestimated. In the presence of a compound or possibly even a lubricant only, the performance of the bare copper-aluminum samples was surprisingly good.

Appendix

The observations made on all test samples are compiled in Tables V to XI inclusive. Since the data contained in these tables form the basis for the conclusions reached, a few explanatory remarks are given. These explanations seem necessary since the tables contain more than just the observed resistances of the test samples.

The test data have been arranged in seven identical tables, one for each sample group. Each table thus contains all the data for a given copper finish with and without the use of a compound, and for both test procedures, including the ageing.

Observed Resistance

The first column in each of the three divisions (no compound, Penetrox "A," "compound B") of each table gives the observed resistance of the joints as it was observed at the different stages of the test. The figures given are the means of the two observations on each sample assembly. The second column shows the increase of the observed resistance, and these two columns do not seem to warrant any further comment, except for the general statement that the consistency of the observations on the two joints forming one sample assembly was always good, and it was in every case permissible to average the two observed values.

Relative Conductivity

Since the observed resistance in itself is not a significant characteristic of the quality of a joint, the third column in the tables gives the electrical relative conductivity of the connection.

Relative conductivity is a significant

Table V. Aluminum versus Copper, Bare, Bright-Dipped Before Installation

Stage of Test	No Compound: Resistance, Microhms, at 20°C	No Compound: Resistance Increase, Per Cent	No Compound: Relative Conductivity, Per Cent	No Compound: Contact Resistance, Microhms Per Sq Inch	Penetrox "A" Compound: Resistance, Microhms, at 20°C	Penetrox "A" Compound: Resistance Increase, Per Cent	Penetrox "A" Compound: Relative Conductivity, Per Cent	Penetrox "A" Compound: Contact Resistance, Microhms Per Sq Inch	Compound B: Resistance, Microhms, at 20°C	Compound B: Resistance, Increase, Per Cent	Compound B: Relative Conductivity, Per Cent	Compound B: Contact Resistance, Microhms Per Sq Inch
Heat Treatment, Assembly Numbers 1, 3, and 5												
Initial measurement	19.7		55.1	18.5	9.4		115.5	0.6	13.0		83.5	7.0
After 624 hours ageing	28.0	42.1	38.8	33.0	10.0	6.3	108.6	1.8	16.2		67.0	12.4
After 8 hours at:												
75°C	29.2	48.2	37.2	35.2	10.0	6.3	108.6	1.8	16.2		24.6	12.4
100°C	35.9	82.2	30.3	46.8	10.1	7.4	107.5	1.9	14.7		13.1	9.9
125°C	32.3	64.0	33.6	40.6	10.6	12.8	102.5	2.8	12.2		−6.2	5.6
150°C	32.9	67.0	33.0	41.6	12.4	31.9	87.6	5.9	11.7		−10.0	4.7
175°C	43.8	111.3	24.9	60.2	11.8	25.5	92.1	4.9	11.0		−15.4	3.5
200°C	146.3	642.6	7.4	239.1	12.2	29.8	89.0	5.5	14.6		12.3	9.7
Corrosion Treatment, Assembly Numbers 2, 4, and 6												
Initial measurement	17.6		61.7	14.8	9.7		112.0	1.2	12.7		85.5	6.4
After 624 hours ageing	25.1	42.6	43.3	27.6	9.7		112.0	1.2	16.1		26.8	12.4
After following hours of corrosion:												
100	27.0	53.4	40.2	31.3	10.1	4.1	107.5	1.9	17.3		36.2	14.5
200	30.6	73.9	35.5	37.6	10.0	3.1	108.6	1.7	17.1		34.6	14.1
300	41.5	135.8	26.2	56.6	10.0	3.1	108.6	1.7	17.7		39.4	15.1
400	46.6	164.8	23.3	64.5	10.3	6.2	105.5	2.3	17.0		33.9	13.9
500	50.0	184.1	21.7	85.3	10.1	4.1	107.5	1.9	17.8		40.2	15.3
600	54.0	206.2	20.1	76.6	10.6	9.3	102.5	2.8	18.2		43.3	16.0
700	56.0	218.2	19.4	81.8	10.6	9.3	102.5	2.8	19.7		55.1	18.6
800	59.4	237.5	18.3	87.7	10.8	11.3	100.6	3.1	19.2		51.2	17.8
900	60.6	244.3	17.9	103.8	10.7	10.3	101.5	3.0	19.0		49.6	17.4
1000	65.4	272.0	16.6	98.2	12.9	33.0	84.2	6.7	19.6		54.3	18.5

Table VI. Aluminum versus Copper, Cadmium-Plated, 0.001 Inch

Stage of Test	No Compound: Resistance, Microhms, at 20°C	No Compound: Resistance Increase, Per Cent	No Compound: Relative Conductivity, Per Cent	No Compound: Contact Resistance, Microhms Per Sq Inch	Penetrox "A" Compound: Resistance, Microhms, at 20°C	Penetrox "A" Compound: Resistance Increase, Per Cent	Penetrox "A" Compound: Relative Conductivity, Per Cent	Penetrox "A" Compound: Contact Resistance, Microhms Per Sq Inch	Compound B: Resistance, Microhms, at 20°C	Compound B: Resistance Increase, Per Cent	Compound B: Relative Conductivity, Per Cent	Compound B: Contact Resistance, Microhms Per Sq Inch
Heat Treatment, Assembly Numbers 7, 9, and 11												
Initial measurement	37.3		29.1	49.4	10.1		107.5	1.8	43.6		24.9	60.3
After 624 hours ageing	56.0	50.1	19.4	81.8	10.9	7.9	99.6	3.3	55.0	26.1	19.7	80.0
After 8 hours at:												
75°C	62.6	67.8	17.4	93.3	11.5	13.9	94.4	4.4	53.4	22.5	20.3	77.3
100°C	70.8	89.8	15.3	107.6	12.1	19.8	89.7	5.4	59.7	36.9	18.1	88.3
125°C	74.3	99.2	14.6	113.7	13.3	31.7	81.6	7.5	60.5	38.8	17.9	89.7
150°C	70.5	89.0	15.4	107.1	10.7	5.9	101.5	3.0	48.7	11.7	22.3	69.1
175°C	26.5	−29.0	40.9	30.5	11.5	13.9	94.4	4.4	38.0	−12.8	28.5	50.5
200°C	97.1	160.3	11.2	153.3	11.7	15.8	92.8	4.8	18.4	−57.8	59.0	16.5
Corrosion Treatment, Assembly Numbers 8, 10, and 12												
Initial measurement	55.2		19.7	80.3	9.3		116.7	0.6	53.5		20.3	77.6
After 624 hours ageing	89.0	61.2	12.2	139.4	10.4	11.8	104.4	2.4	68.7	28.4	15.8	103.9
After following hours of corrosion:												
100	63.6	15.2	17.1	95.1	10.6	14.0	102.4	2.8	66.4	24.1	16.3	99.9
200	68.2	24.3	15.9	103.1	10.3	10.8	105.4	2.3	62.5	16.8	17.3	93.1
300	74.1	34.2	14.6	113.3	10.3	10.8	105.4	2.3	61.9	15.7	17.5	92.1
400	69.7	26.3	15.5	105.7	10.9	17.2	99.6	3.3	59.5	11.2	18.2	87.9
500	78.8	42.8	13.7	121.5	11.0	18.3	98.7	3.5	58.5	9.3	18.5	86.2
600	88.4	60.1	12.2	138.2	11.0	18.3	98.7	3.5	60.1	12.3	18.0	89.0
700	87.4	58.3	12.4	136.5	10.9	17.2	99.6	3.3	59.8	11.8	18.1	88.4
800	88.6	60.5	12.2	138.6	11.5	23.7	94.4	4.4	57.4	7.3	18.9	84.3
900	90.3	63.6	12.0	141.5	11.2	20.4	96.9	3.8	58.1	8.5	18.7	85.5
1000	95.2	72.4	11.4	150.0	12.1	30.1	89.8	5.5	58.2	8.7	18.7	85.7

Table VII. Aluminum versus Copper, Cadmium-Plated, 0.0003 Inch

Stage of Test	No Compound: Resistance, Microhms, at 20°C	No Compound: Resistance Increase, Per Cent	No Compound: Relative Conductivity, Per Cent	No Compound: Contact Resistance, Microhms Per Sq Inch	Penetrox "A" Compound: Resistance, Microhms, at 20°C	Penetrox "A" Compound: Resistance Increase, Per Cent	Penetrox "A" Compound: Relative Conductivity, Per Cent	Penetrox "A" Compound: Contact Resistance, Microhms Per Sq Inch	Compound B: Resistance, Microhms, at 20°C	Compound B: Resistance Increase, Per Cent	Compound B: Relative Conductivity, Per Cent	Compound B: Contact Resistance, Microhms Per Sq Inch
Heat Treatment, Assembly Numbers 13, 15, and 17												
Initial measurement	69.2		15.6	104.8	10.0		108.6	1.7	39.3		27.6	52.8
After 624 hours ageing	87.7	26.7	12.3	137.0	10.4	4.0	104.4	2.4	58.8		49.6	85.1
After 8 hours at:												
75°C	89.9	29.9	12.1	140.8	11.2	12.0	97.0	3.8	46.7	18.8	23.3	65.6
100°C	84.3	21.8	12.9	131.1	11.8	18.0	92.1	4.8	40.2	2.3	27.0	54.5
125°C	77.3	11.7	14.1	118.9	12.9	29.0	84.2	6.8	26.3	−33.1	41.3	30.1
150°C	61.4	−11.3	17.7	91.2	14.8	48.0	73.4	10.1	19.0	−51.7	57.2	17.4
175°C	79.8	15.3	13.6	123.3	10.5	5.0	103.4	2.6	13.2	−66.4	82.3	7.3
200°C	183.1	164.5	5.9	303.0	10.8	8.0	100.6	3.1	11.9	−69.7	91.2	5.0
Corrosion Treatment, Assembly Numbers 14, 16, and 18												
Initial measurement	54.3		20.0	78.8	10.3		105.4	1.8	43.7		24.9	60.3
After 624 hours ageing	67.0	23.4	16.2	100.9	11.4	10.7	95.3	4.2	65.0	48.8	16.7	97.6
After the following hours of corrosion:												
100	50.3	− 7.4	21.6	71.9	12.1	17.5	89.8	5.4	65.9	50.8	16.5	99.1
200	53.1	− 2.2	20.5	76.8	12.0	16.5	90.5	5.2	63.4	45.1	17.1	94.7
300	53.0	− 2.3	20.5	76.6	12.4	20.4	87.6	5.9	61.7	41.2	17.6	91.8
400	50.4	− 7.2	21.6	72.1	13.2	28.2	82.3	7.3	61.5	40.7	17.7	91.4
500	54.7	0.7	19.9	79.6	13.4	30.1	81.1	7.7	61.9	41.6	17.5	92.1
600	57.3	5.5	19.0	84.1	14.1	36.9	77.0	8.9	65.3	49.4	16.6	98.0
700	57.0	5.0	19.1	83.6	14.8	43.7	73.4	10.1	64.7	48.1	16.8	95.4
800	57.6	6.1	18.9	84.6	14.8	43.7	73.4	10.1	63.9	46.2	17.0	95.6
900	58.9	8.5	18.4	86.9	15.2	47.6	71.5	10.8	64.4	47.4	16.9	96.5
1000	60.9	12.2	17.8	90.4	16.6	61.2	65.4	13.2	64.3	47.1	16.9	96.4

measure of the quality of the joint with respect to its electrical resistance. It is expressed as the ratio between the resistance of an imaginary (or real) conductor with the same dimensions and made from the same material as individual members forming the joint, and the actual joint resistance. Thus, if the relative conductivity of a joint is 100 per cent, this indicates that the joint has a resistance equal to the resistance of the unbroken, single conductor of the same dimension and made from the same material. Similarly, a relative conductivity of 10 per cent would indicate that the joint resistance is ten times the resistance of the unbroken single conductor of the same dimensions. For the purpose of computing the relative conductivity as given in the table, the resistance of the reference conductor was determined in the following manner, which is illustrated also in Figure 2.

The joint resistance was measured from P to P_1 across a distance of 3 inches (see Figure 1). The bus bars forming the joint were 1 by 1/4 inches each. The overlap distance was 2 inches and was centered within the 3-inch total measuring distance. The reference conductor therefore as shown in Figure 2 would be a single bus bar, with a measuring distance (P–P_1) of 3 inches. Since the joint is between aluminum and copper bars, we can imagine the reference conductor to be composed of two equal lengths, $1^1/_2$ inches of the measuring distance of aluminum, and $1^1/_2$ inches of copper bar 1 by 1/4 inches in area. The resistance of such an imaginary bar would then become:

$$R = \frac{r_{al} \times 1.5}{0.25} + \frac{r_{cu} \times 1.5}{0.25}$$

where

r_{al} = resistivity of hard rolled aluminum, or 1.1134 microhms per inch at 20 degrees centigrade, equal to 60.97 per cent International Annealed Copper Standard (IACS).

and

r_{cu} = resistivity of hard rolled copper, or 0.6969 microhm at 20 degrees centigrade, equal to 97.4 per cent IACS

Table VIII. Aluminum versus Copper, Electrotinned, 0.0003 Inch

Stage of Test	No Compound: Resistance, Microhms, at 20°C	No Compound: Resistance Increase, Per Cent	No Compound: Relative Conductivity, Per Cent	No Compound: Contact Resistance, Microhms Per Sq Inch	Penetrox "A" Compound: Resistance, Microhms, at 20°C	Penetrox "A" Compound: Resistance Increase, Per Cent	Penetrox "A" Compound: Relative Conductivity, Per Cent	Penetrox "A" Compound: Contact Resistance, Microhms Per Sq Inch	Compound B: Resistance, Microhms, at 20°C	Compound B: Resistance Increase, Per Cent	Compound B: Relative Conductivity, Per Cent	Compound B: Contact Resistance, Microhms Per Sq Inch
Heat Treatment, Assembly Numbers 19, 21, and 23												
Initial measurement	13.3		81.7	7.5	9.5		114.3	0.9	10.1		107.5	1.8
After 624 hours ageing	18.4	38.3	59.0	16.4	10.2	7.4	106.5	2.1	11.4	12.9	95.3	3.9
After 8 hours at:												
75°C	23.8	78.9	45.6	25.8	11.1	16.8	97.9	3.7	12.3	21.8	88.3	7.5
100°C	29.4	121.1	36.9	35.5	13.9	46.3	78.1	3.9	15.7	55.4	69.2	11.7
125°C	23.3	75.2	46.6	24.9	19.3	103.2	56.3	17.9	11.8	16.8	92.1	6.6
150°C	13.9	4.5	78.1	8.5	19.1	101.1	56.9	17.6	10.2	1.0	106.5	2.1
175°C	12.5	−6.1	86.9	6.1	10.1	6.3	107.5	1.9	9.0	−10.9	120.7	
200°C	19.5	46.6	55.7	18.2	12.9	35.7	84.2	6.7	10.5	4.0	103.4	2.7
Corrosion Treatment, Assembly Numbers 20, 22, and 24												
Initial measurement	15.3		71.0	10.9	9.4		115.5	0.6	11.5		94.4	4.2
After 624 hours ageing	19.0	24.2	57.2	17.3	9.9	5.3	109.7	1.5	13.4	16.5	81.0	7.6
After the following hours of corrosion:												
100	23.9	56.2	45.4	25.9	9.9	5.3	109.7	1.5	14.4	25.2	75.4	9.4
200	29.2	90.8	37.2	35.2	10.7	13.8	101.5	3.0	14.5	26.1	74.9	9.6
300	32.6	113.1	33.3	41.1	10.0	6.4	108.6	1.7	16.6	44.3	65.4	13.2
400	33.1	116.3	32.8	40.2	11.7	24.5	92.8	4.7	16.8	46.1	64.7	13.6
500	37.3	143.8	29.1	49.3	11.4	21.3	95.3	4.2	17.0	47.8	63.9	13.9
600	40.3	163.4	27.0	54.5	12.9	37.2	84.2	6.8	19.4	68.7	56.0	18.4
700	42.3	176.5	25.7	58.0	12.0	27.7	90.5	5.2	18.3	59.1	59.4	16.2
800	43.3	183.0	25.1	59.7	12.1	28.7	89.8	5.4	20.1	74.8	54.0	19.3
900	44.8	192.8	24.2	62.3	12.3	30.9	88.3	5.7	21.9	90.4	49.6	22.5
1000	47.1	207.8	23.1	66.4	14.1	50.0	77.0	8.8	25.4	120.9	42.8	28.5

Table IX. Aluminum versus Copper, Electrotinned, 0.0003 Inch, and Flowed

Stage of Test	No Compound: Resistance, Microhms, at 20°C	No Compound: Resistance Increase, Per Cent	No Compound: Relative Conductivity, Per Cent	No Compound: Contact Resistance, Microhms Per Sq Inch	Penetrox "A" Compound: Resistance, Microhms, at 20°C	Penetrox "A" Compound: Resistance Increase, Per Cent	Penetrox "A" Compound: Relative Conductivity, Per Cent	Penetrox "A" Compound: Contact Resistance, Microhms Per Sq Inch	Compound B: Resistance, Microhms, at 20°C	Compound B: Resistance Increase, Per Cent	Compound B: Relative Conductivity, Per Cent	Compound B: Contact Resistance, Microhms Per Sq Inch
Heat Treatment, Assembly Numbers 25, 27, and 29												
Initial measurement	10.8		100.6	3.1	10.0		108.6	1.7	9.3		116.7	0.5
After 624 hours ageing	12.7	17.6	85.5	6.4	11.0	10.0	98.7	3.5	10.5	12.9	103.4	2.6
After 8 hours at:												
75°C	16.1	49.1	67.5	12.4	12.0	20.0	90.5	5.2	10.4	11.8	104.4	2.4
100°C	13.2	22.2	82.3	7.3	12.5	25.0	86.8	6.1	10.4	11.8	104.4	2.4
125°C	12.1	12.0	89.7	5.4	12.1	21.0	89.7	5.4	9.8	5.4	110.8	1.4
150°C	11.4	5.6	95.2	4.2	9.5	−5.0	114.3	0.9	9.3		116.7	0.5
175°C	10.5	−2.8	103.4	2.6	8.8	−12.0	123.4		8.8	−5.4	123.4	
200°C	14.7	36.1	73.8	10.0	9.0	−10.0	120.7		9.1	−2.3	119.3	0.2
Corrosion Treatment, Assembly Numbers 26, 28, and 30												
Initial measurement	10.1		107.5	1.9	9.6		113.1	1.0	9.5		114.3	0.9
After 624 hours ageing	10.7	5.9	101.5	3.0	10.7	11.5	101.5	3.0	11.1	16.8	97.8	3.6
After the following hours of corrosion:												
100	11.1	9.9	97.8	3.7	10.4	8.3	104.4	2.4	11.3	18.9	96.1	4.0
200	11.6	14.9	93.6	4.5	10.4	8.3	104.4	2.4	10.9	14.7	99.6	3.3
300	11.8	16.8	92.0	4.9	10.2	6.3	106.4	2.1	9.9	4.2	109.7	1.6
400	12.4	12.9	87.5	5.9	11.1	15.6	97.8	3.7	11.4	20.0	95.3	4.2
500	13.6	34.7	79.8	8.0	12.0	25.0	90.5	5.2	11.9	25.3	91.2	5.0
600	14.1	39.6	77.0	8.9	11.6	20.8	93.6	4.5	12.6	32.6	86.2	6.3
700	14.4	42.6	75.4	9.4	11.4	18.8	95.2	4.2	12.5	31.6	86.8	6.1
800	15.1	49.5	71.9	10.6	12.1	26.0	89.7	5.4	13.5	42.1	80.4	7.8
900	15.4	52.5	70.5	11.1	12.3	28.1	88.3	5.7	13.7	44.2	79.2	8.2
1000	16.7	65.3	65.0	13.3	13.8	43.8	78.7	8.5	15.1	58.9	71.9	10.6

Substituting the foregoing constants,

$$R=\frac{1.1134\times1.5}{0.25}+\frac{0.6969\times1.5}{0.25}=4.182+6.68$$

$$=10.862 \text{ microhms at 20 degrees centigrade}$$

The value of 10.86 microhms was used in the tabulations for the computation of the relative conductivity.

Contact Resistance

The fourth and last column in the tables gives the contact resistance of the connections.

Before discussing the method by which the contact resistance was determined, a brief definition of the term is given.

The total resistance of an overlap joint such as discussed here, can be broken down into three different partial resistances:

1. The metallic resistance, which is entirely a function of the particular metal used and its dimensions. Of course this resistance depends also upon the temperature at the time of observation, but this can be disregarded in this discussion since all measurements are corrected for the reference temperature of 20 degrees centigrade.

2. The resistance in an overlap joint which is due to the distortion of the current flow, the streamline effect. This resistance is independent of the physical properties of the conductors, and is determined by the configuration of the joint, that is the sectional area of the conductor, and the length of overlap.

3. The contact resistance, which is the resistance that the current has to overcome in order to pass from one bar to the other across the facing surfaces. This resistance is dependent upon several factors, such as the finish and preparation of the contact surfaces, the contact pressure, and the possible presence of a compound. Since it is generally accepted that current passes between two members of a joint at a number of more or less isolated points, there is an added resistance (included in the over-all term of contact resistance) due to the constriction of the current flow.

It is evident that the metallic resistance can be calculated since the resistivities of the metals and the dimensions of the bars are known. The effect of the streamlining of the current due to the overlap joint can be approximated by data which have been established for such bus bar joints.[3] In this particular investigation however, the effect of the streamlining has been determined experimentally as will be discussed

Table X. Aluminum versus Copper, Hot Tin-Dipped

Stage of Test	No Compound: Resistance, Microhms, at 20°C	No Compound: Resistance Increase, Per Cent	No Compound: Relative Conductivity, Per Cent	No Compound: Contact Resistance, Microhms Per Sq Inch	Penetrox "A" Compound: Resistance, Microhms, at 20°C	Penetrox "A" Compound: Resistance Increase, Per Cent	Penetrox "A" Compound: Relative Conductivity, Per Cent	Penetrox "A" Compound: Contact Resistance, Microhms Per Sq Inch	Compound B: Resistance, Microhms, at 20°C	Compound B: Resistance Increase, Per Cent	Compound B: Relative Conductivity, Per Cent	Compound B: Contact Resistance, Microhms Per Sq Inch
Heat Treatment, Assembly Numbers 37, 39, and 41												
Initial measurement	12.2		89.0	5.6	10.0		108.6	1.7	10.1		107.5	1.9
After 624 hours ageing	16.8	37.7	64.7	13.6	11.0	10.0	98.7	3.5	12.4	22.8	87.6	5.9
After 8 hours at:												
75°C	24.4	100.0	44.5	26.8	11.3	13.0	96.1	4.0	13.8	36.6	78.7	8.4
100°C	19.9	63.1	54.6	10.9	12.0	20.0	90.5	5.2	12.5	23.8	86.9	6.1
125°C	16.6	36.1	65.4	13.2	11.5	15.0	94.5	4.4	11.3	11.9	96.1	4.0
150°C	17.1	40.2	63.5	14.1	10.1	1.0	107.5	1.9	11.3	11.9	96.1	4.0
175°C					9.2	−8.0	118.1	0.3	9.2	−8.9	118.1	0.3
200°C	19.0	55.7	57.2	17.4	9.4	−6.0	115.5	0.7	9.5	−5.9	114.3	0.9
Corrosion Treatment, Assembly Numbers 38, 40, and 42												
Initial measurement	10.8		100.6	3.0	10.1		107.5	1.8	10.5		103.4	2.7
After 624 hours ageing	14.5	34.3	74.9	9.7	11.0	8.9	98.7	3.3	11.2	6.7	97.0	3.9
After the following hours of corrosion:												
100	15.5	43.5	70.1	11.3	10.5	4.0	103.4	2.6	10.4	−1.0	104.4	2.4
200	16.4	51.9	66.2	12.9	11.1	9.9	97.9	3.7	11.7	11.4	92.8	4.7
300	17.7	63.9	61.4	15.1	11.0	8.9	98.7	3.5	11.7	11.4	92.8	4.7
400	18.7	73.1	58.1	16.9	11.6	14.9	114.9	4.5	12.4	18.1	87.6	5.9
500	19.9	84.3	54.6	10.9	11.9	17.8	91.3	5.0	14.1	34.3	77.0	8.9
600	20.9	93.5	52.0	20.7	12.5	23.8	86.9	6.1	13.7	30.5	79.3	8.2
700	21.6	100.0	50.3	21.9	12.2	20.8	89.0	5.6	13.5	28.6	80.5	7.8
800	22.8	111.1	47.6	24.0	12.4	22.8	87.6	5.9	14.2	35.3	76.5	9.1
900	23.5	117.6	46.2	25.2	12.9	27.7	84.2	6.8	14.6	39.0	74.4	9.7
1000	26.4	144.4	41.1	30.3	14.5	43.5	74.9	9.7	16.6	58.1	65.4	13.3

Table XI. Aluminum versus Copper, Zinc-Plated, 0.0002 Inch

Stage of Test	No Compound: Resistance, Microhms, at 20°C	No Compound: Resistance Increase, Per Cent	No Compound: Relative Conductivity, Per Cent	No Compound: Contact Resistance, Microhms Per Sq Inch	Penetrox "A" Compound: Resistance, Microhms, at 20°C	Penetrox "A" Compound: Resistance Increase, Per Cent	Penetrox "A" Compound: Relative Conductivity, Per Cent	Penetrox "A" Compound: Contact Resistance, Microhms Per Sq Inch	Compound B: Resistance, Microhms, at 20°C	Compound B: Resistance Increase, Per Cent	Compound B: Relative Conductivity, Per Cent	Compound B: Contact Resistance, Microhms Per Sq Inch
Heat Treatment, Assembly Numbers 43, 45, and 47												
Initial measurement	28.2		38.5	33.4	10.0		108.6	1.7	14.5		74.9	9.6
After 624 hours ageing	38.6	36.9	28.1	51.5	10.5	5.0	103.4	2.6	28.9	99.3	37.6	34.6
After 8 hours at:												
75°C	59.9	112.4	18.1	88.6	10.4	4.0	104.4	2.4	50.1	245.5	21.7	71.6
100°C	82.5	192.8	13.2	128.0	10.4	4.0	104.4	2.4	65.8	353.8	16.8	98.9
125°C	106.6	278.0	10.2	170.0	11.0	10.0	98.7	3.5	55.8	284.8	19.5	81.5
150°C	31.2	10.6	34.8	38.7	14.4	44.0	75.4	9.4	26.9	85.5	40.4	31.2
175°C	138.2	390.1	7.9	224.9	14.6	46.0	74.4	9.7	18.1	24.8	60.0	15.8
200°C	490.8	1640.4	2.2	838.8	19.5	95.0	55.7	18.3	42.4	192.4	25.6	58.2
Corrosion Treatment, Assembly Numbers 44, 46, and 48												
Initial measurement	25.4		42.8	28.6	10.4		104.4	2.4	14.6		74.4	9.7
After 624 hours ageing	38.5	51.6	28.2	51.4	10.5	1.0	103.4	2.6	26.8	83.6	40.5	30.9
After the following hours of corrosion:												
100	54.8	115.7	19.8	78.0	10.2	−1.9	106.5	2.1	37.1	154.0	29.3	48.9
200	71.9	183.1	15.1	109.5	10.3	−1.0	105.5	2.3	43.2	195.9	25.1	57.8
300	93.9	269.7	11.6	147.8	10.8	3.8	100.6	3.1	49.2	237.0	22.1	70.0
400	102.7	304.3	10.6	163.1	10.4		104.4	2.4	51.8	254.8	21.0	74.5
500	120.8	374.8	9.0	194.3	11.2	7.7	97.0	4.0	41.4	183.6	26.2	56.4
600	134.4	429.1	8.1	218.3	12.1	16.3	89.8	5.4	58.3	299.3	18.6	85.8
700	137.1	439.8	7.9	223.0	10.6	1.9	102.5	2.8	63.9	337.7	17.0	95.6
800	142.8	462.2	7.6	232.9	10.9	4.8	99.7	3.3	67.0	358.9	16.2	101.0
900	147.1	479.1	7.4	240.4	11.1	6.7	97.9	3.7	70.5	382.9	15.4	107.1
1000	161.6	536.2	6.7	265.7	11.4	9.6	95.3	4.2	72.9	399.3	14.9	111.2

later on. The contact resistance, and that is the resistance listed in the tables, can then be determined as the difference between the measured resistance, which is the sum of all three partial resistances, and the sum of the metallic resistance plus the streamline resistance.

We shall now discuss how the sum of the metallic resistance plus the streamline resistance of an overlap joint such as was used for the experiment discussed here, can be determined by a simple experiment with a model connection. In Figure 3, two models are shown, one being the same overlap joint as used in test (A), the other being a solid block of metal having the same dimensions as the overlap joint (B). As was stated earlier, the streamline resistance is independent of the material and resistivity of the component parts of a joint.

Therefore, if we have determined the added resistance due to current flow distortion for a particular configuration we can use this value regardless of whether we deal with copper-to-copper, aluminum-to-aluminum, or copper-to-aluminum connections. It becomes evident that if we measured the resistance of the solid block (B) which has exactly the same dimensions as the joint (A), the same hardware and the same measuring distance, then this resistance is the sum of the metallic resistance plus the added resistance due to streamlining.

Further, if we now consider the resistance of joint (A) consisting of two separate bars made from the same material and also from the same material as the solid block (B), then this resistance contains just one more item, and that is the actual contact resistance.

All the previous considerations have been made under the assumption that the two bars are in intimate contact, which can be assumed considering the comparatively small contact area and the comparatively high contact pressure. The resistance of the solid joint can be measured, and models can be made from aluminum and from copper. The imaginary resistance of a solid copper-to-aluminum joint then becomes the mean of the resistance of the copper joint and the resistance of the aluminum joint, since streamlining is not dependent on physical properties of the conductors used. Then, if we deduct this resistance from the measured resistance of any of our samples, the difference is the contact resistance.

This contact resistance in the tables is given per square inch. The reason for this was to make comparisons easily possible with data available in the literature on contact resistance. Otherwise, since all samples had the same contact areas, the total contact resistances would have been equally comparable. In the following the actual figures, as they were determined on the models, are given. These figures were checked against the easily computed

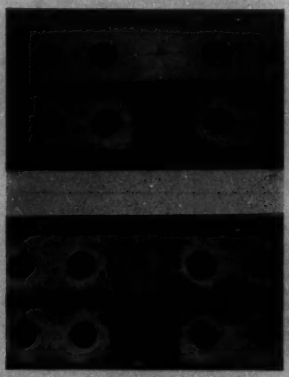

Figure 9. Flowed tin, no compound

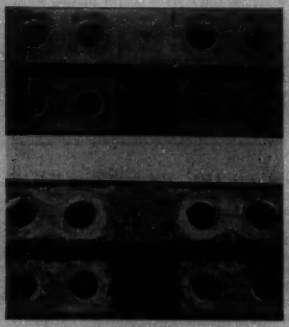

Figure 10. Flowed tin, Penetrox "A"

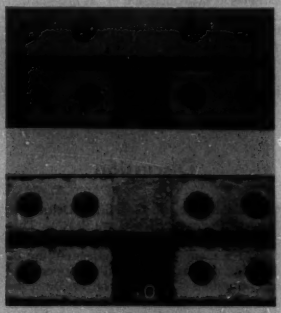

Figure 11. Flowed tin, "compound B"

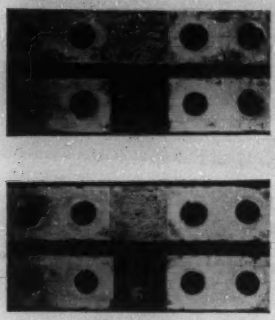

Figure 12. Bare copper

metallic resistance and data on the streamline resistance available from investigations on this subject. It was found that the actually measured resistances were in close agreement with the computed values.

Measured resistance $P-P_1$ on model B (copper), microhms, 6.925
Calculated resistance, microhms, 6.349
Difference due to distortion of current flow microhms, 0.576
Resistance of identical model (B) made from aluminum instead of copper, microhms, 11.063

Average of resistances of copper and aluminum models

$$\frac{6.925+11.063}{2}=8.994 \text{ microhms}$$

A resistance of nine microhms was used to compute the contact resistance in the tables.

Physical Effects of the Test Procedures on the Samples

An investigation of the character covered in this paper would hardly be complete without thorough examination of the appearance of the bus bars, especially as they were affected by the prolonged corrosion test. Such an examination was made of representative samples and they are shown in Figures 9, 10, 11, 12, and 13.

Even superficial examination of the samples at the conclusion of the test showed two marked effects: samples tested at elevated temperatures had discolored completely depending on the finish of the copper bars (the aluminum bar which was always bare had mainly darkened more or less uniformly). Probably the darkest color was shown by the zinc-plated samples. Bare copper stood up comparatively well with only slight dulling of the initially bright surface. Cadmium also remained fairly clean, but the electrotinned samples changed to a rather dark color.

The effects of corrosion were strikingly apparent on all aluminum bars. Since aluminum is anodic, it is natural that it would corrode rather than the cathodic, protected copper.

Figures 9, 10, and 11, show the contact surfaces of electrotinned and flowed samples after conclusion of the test procedures. The samples tested at elevated temperature have been arranged on top of the corroded samples. Referring to these three illustrations, samples tested at elevated temperature show some spotted areas on the copper, but of course no corrosion. On the corroded samples, deep pitting is especially noticeable at the edges and in the center of the aluminum bar. On all samples the area of intimate contact is clearly visible around the holes and between them, and the copper bars still show some of the flowed-tin coating left.

Samples with "compound B" always remained comparatively bright and this was true regardless of the finish on the copper. While the preserving qualities of this compound are excellent, it lacks the penetrating properties of Penetrox "A," and therefore does not produce the low-contact resistance of the latter.

Figure 12 shows bare copper in contact with bare aluminum, the upper sample (number 4) with Penetrox "A," the lower sample (number 6) with "compound B," both after corrosion. The serious pitting of the aluminum is clearly shown on the aluminum bars, and again the "compound B" sample shows the well-preserved copper surfaces. Yet, the average joint resistance of the "compound B" sample at the end of the test was approximately 50 per cent greater than the resistance of the Penetrox samples.

Figure 13 shows the flowed-tin coating with a magnification of 500. The top illustration was taken on sample number 25, which did not have any compound and was tested at elevated temperature. On this photograph, the bottom area shows the copper bars. The white area in the center represents the flowed tin coating, while the top layer is copper plating which had to be applied in preparation of the sample for the metallographic examination. It can be seen that this tin layer is well preserved and continuous. The bottom illustration was taken from sample number 28 after the corrosion test. This sample also had a flowed tin finish and Penetrox "A" compound. The tin layer is still visible, but it is quite thin and not continuous.

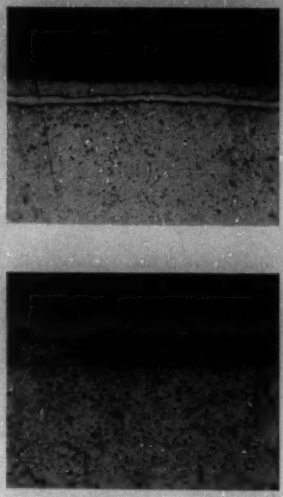

Figure 13. Flowed tin (500× magnification)

The heavy dark layer between the tin and the top copper very likely consists mainly of corrosion products. There is a dark line approximately 1/32-inch thick alongside the bare copper on both flowed tin samples. This line indicates probable diffusion of copper into the tin, possibly as a consequence of the flowing operation. It is interesting in comparing these two photomicrographs to note that the joint resistance of the two samples was practically alike.

References

1. United States Patent Number 2,423,290, 1947.

2. The Effects of Temperature on Plated Coatings, P. Ritzenthaler, *Metal Industry* (London, England), October 1939, page 409.

3. The Efficiency of Overlapping Joints in Copper and Aluminum Bus-Bar Conductors, S. W. Melsom, H. C. Booth. *Journal*, Institution of Electrical Engineers (London, England), volume 60, 1922, page 889.

Discussion

C. S. Beattie and S. C. Killian (Delta-Star Electric Company, Chicago, Ill.): This paper makes a worthwhile contribution toward a more complete understanding of the long debated aluminum-to-copper contact question. Bonwitt is to be congratulated on his paper and on the very complete variety of protection tested. He has not, however, tested many samples of the same kind. Not until several dozen tests are made on one combination and averaged, can we rely on the result.

From Table I of the paper, Effect of Elevated Temperatures, it is noted that the final resistances varied from 9.0 microhms to only 19.5 microhms for the first 16 combinations tested. While we can say that one combination has twice the resistance of the other, the actual resistances are so minute that a difference of 9 microhms is microscopic and is of no practical consequence. Also since all 16 of the values are so very close to one another, it is very possible that a larger number of tests or a slightly different method of contact preparation on the same combinations might completely rearrange the table. It is also interesting to note that the cadmium-plated combinations had the largest decrease in resistance at the elevated temperature.

In Table II, Effect of Corrosive Atmosphere, the spread between the first 13 final resistances is not large, 11.4 to 26.4

microhms, a total difference of only 15 microhms. There is little to choose between these in actual practice and again it is interesting to note that the cadmium plated combinations 15 and 16, though ending at 58.2 and 60.9, started at 53.5 and 54.3 microhms, a total rise in resistance less than many of those combinations ranked above it. We know from many years of actual field experience that these cadmium combinations are quite stable and over years of time will continue to stay stable while those ranked higher in the author's table might continue to increase in resistance if the tests were run longer than 1,000 hours. If the cadmium-plated combinations using compound "B" were prepared according to recommended practice, the actual resistances measured would be considerably lower than those indicated in the table.

While a current of only ten amperes as used in the tests does keep down temperature rise, and save laboratory corrections on resistance measurements, larger currents might produce very different results. It has been shown that the resistance of corroded joints falls with an increase in current. The capacity of a 1/4 inch by 1 inch bar is far in excess of ten amperes and it is possible that the test should have been conducted at current values more nearly approaching those encountered in actual practice.

The data submitted in the paper represent an enormous amount of work and again assures the industry that a reliable copper-to-aluminum connection can be made if ordinary precautions are taken. Further testing and years of service experience under adverse atmospheric conditions may some day point to the one best method of preparing the aluminum-to-copper joint.

W. F. Bonwitt: The comments by Beattie and Killian are appreciated. Before discussing them, it should be stated that the tests described were made to obtain a comparative grading of the various platings or coatings under more or less controlled laboratory conditions. They were not intended to replace or eliminate observation in actual service. Such tests are being conducted with the best samples obtained from the original tests. For this reason and for obvious practical considerations, the number of identical samples was held to a minimum. However, the agreement between identical samples was so close in all cases that the small number of samples was not considered to impair the significance of the results too much.

As far as the effect of elevated temperatures on the resistance of these samples is concerned, it is quite correct that these effects were almost negligible for a majority of the samples. However, it was mostly true for samples which had a compound applied and this fact seems significant. The only coatings which performed satisfactorily even without a compound were hot tin dip, electrotinned, and electrotin flowed.

As far as the effects of the corrosion procedures are concerned, there is no doubt that cadmium is quite stable, maybe even more so than other coatings tested. However, this is more than balanced by the fact that cadmium, except when used with Penetrox "A," produces consistently a very high resistance level, while the tin group as a whole will be satisfactory even in dry condition. This is important from the practical point of view since the application of a compound is something which cannot be completely relied upon. It should also be considered that a 1,000-hour continuous corrosion test under the conditions described is extremely severe. It is impossible to estimate just how many years of service under various atmospheric conditions this would equal, but to judge from the appearance of the aluminum members, the author has rarely seen aluminum connectors or conductors with many years of service which were in a similar state of disintegration. Since the resistances of the samples of the tin group were still well below the resistances of the samples of the cadmium group after such a severe test, I feel that it is fair from a practical point of view to rate the tin above the cadmium, despite the fact that the cadmium samples were more stable.

As to the question of conducting the tests at such loads that a substantial temperature rise would have resulted, past experience has shown that the great experimental difficulties of such an arrangement when dealing with large numbers of samples, are in no proportion to the difference in the results obtained. However, in conducting field tests with the combinations selected as a result of the tests, proper consideration was given to such experiments.

The Naval Ordnance Laboratory Six-Trace Cathode-Ray Oscillograph

P. L. EDWARDS
NONMEMBER AIEE

TESTING of ordnance often requires the recording of high-speed steady-state and transient phenomena both in the field and laboratory. The portable multichannel magnetic-string type oscillograph is admirably suited for recording many signals simultaneously, but does not possess the high-frequency response, the high-input impedance, or the high sensitivity often required. A natural consideration is the use of amplifiers for overcoming these difficulties. An amplifier can easily have the high-input impedance and the high gain necessary, but for the over-all pass band to be wider than two or three kc requires that the amplifier be carefully compensated. The undesirability of a large direct current flowing through the magnetic-string element at all times and the usual instability of high gain d-c amplifiers often dictate that this be an a-c amplifier. Even with a carefully compensated amplifier, the upper frequency limit is about ten kc per second.

The use of cathode-ray tubes as the indicating elements of recording oscillographs is another approach to the solution of these problems. These tubes may be used over a very wide frequency range, and though almost always requiring amplifiers, these amplifiers need a flat frequency response rather than the complicated response required to compensate for the magnetic-string elements. Recording oscillographs have been in use for a number of years. However, those available during the recent war for recording several signals simultaneously were ordinarily very large and bulky, making field use difficult, if not impossible.

The Naval Ordnance Laboratory six-trace cathode-ray oscillograph *6TCRO* is a portable, general purpose oscillograph designed to eliminate the previously mentioned difficulties. With this instrument it is possible to record six signals and two timing traces simultaneously. Six 2-inch cathode-ray tubes are each driven by a high-gain high-input impedance d-c amplifier with a flat amplitude response to 40,000 cycles per second. Two additional 2-inch tubes are employed for recording timing signals which are internally produced. The indications of the cathode-ray tubes are recorded on a moving strip of 35-millimeter film.

Design work on the *6TCRO* was begun in the summer of 1944 and was essentially finished by early 1946. Ten units built by the laboratory were completed in 1947. Subsequent application has proved its utility, and has resulted in such a demand for use of the instrument by various government laboratories that a contract is being processed for construction of additional units.

In this paper shall be considered the electrical block diagram, Figure 1; the physical arrangement of parts, Figures 2, 3, 4, 5, and 6; the different modes of operation; and finally some of the uses for which the instrument has been employed.

Electrical Circuits

Figure 1 shows a circuit block diagram for the *6TCRO*.

The power supply (see also Figure 2) consists of electronically regulated +460 and −340 volt supplies and an a-c regulated accelerating voltage supply. The a-c voltage stabilizer also regulates the filament voltage for the tubes of the first stage in the amplifiers. The power consumption is 500 watts at 115 volts and 60 cycles per second.

Each amplifier (see Figures 1, 3, and 4) has a single-sided input into the first stage, a common-cathode-resistor phase inverter,[1] which drives the second push-pull stage. The second stage drives the horizontal deflecting plates of the cathode-ray tube. Each d-c amplifier has a gain of 8,000, an input impedance of 0.1 megohm, and a frequency response which is flat from zero to 40,000 cycles per second and is down 6 decibels at 100,000 cycles per second. The well-regulated power supply, the regulated filament voltage on the first stage, plus a careful selection of tubes has led to an amplifier relatively free from microphonics and with a spot drift, after a warm-up period of 30 minutes, of less than 1/4 inch in 20 minutes.

Each channel has individual controls for gain, intensity, focus, and vertical and horizontal positioning. The gain can be varied so that from 0.01 to 250 volts rms will give full scale deflection. There is also a common intensity control. Since the phosphorescent screens of the cathode-ray tubes are damaged in time by intense electron beams, the beams are normally deflected off the tube faces by means of a voltage applied to the vertical deflecting plates. When the clutch to engage the film drive is operated, a switch is closed which removes this voltage and positions the spots for recording. For preliminary adjustments, a toggle switch in parallel with the clutch switch is used to center the beams.

A means for voltage calibration of the channels is contained within the *6TCRO*. The output of a "parallel-T" sine-wave oscillator,[2] Figure 1, may be attenuated to any desired value, measured by an internal vacuum-tube voltmeter, and applied to the inputs of the amplifier channels. A switch is provided so that the

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P. L. Edwards is with the Naval Ordnance Laboratory, Silver Springs, Maryland.

G. L. Beyer, Ward Shepard, A. F. Robertson, and M. A. Serrell were responsible for the original design of the instrument. Other members of the Instrumentation Subdivision of the Mechanical Evaluation Division made contributions during the construction and improvement of the instrument.

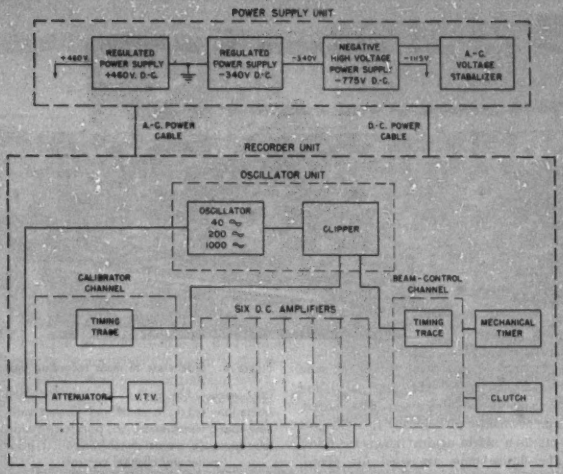

Figure 1 (above). Six-trace cathode-ray oscillograph—circuit block diagram

Figure 2 (left). The six-trace cathode-ray oscillograph assembled for operation

attenuated output of the oscillator may be fed into the amplifiers without the need for external leads.

For timing purposes, the output of the oscillator is clipped, amplified, and applied to the two timing cathode-ray tubes previously mentioned. The oscillator frequencies, and thus the timing frequencies, may be 40, 200, or 1,000 cycles per second.

Physical Arrangement

Figure 2 shows the *6TCRO* in use. The main recording unit is on the table and the power supply is beneath the table. Two power cables connect the recorder to the power supply.

The main unit, including a 24-pound cover not shown, weighs 190 pounds and is 22 by 22 by 23 inches. The power supply weighs 80 pounds and is 10 by 15 by 18 inches.

Figure 3 shows a cross section of the main unit. The light from the eight cathode-ray tubes, six for recording and two for timing, is reflected by the recording mirror to a Baltar 25 millimeter focal length lens which focuses the light from the fluorescent spots on the moving 35 millimeter film. For viewing, the light is reflected from the large view mirror to the small view mirror and from there to the rotating mirror which provides a visual sweep if desired.

Figure 4 shows a rear view of the front panel. The cathode-ray tubes for the timing traces are mounted on the two end channels (calibration channel and beam-control channel) and the tubes on the other channels are for signal recording.

Figure 3 (below). Main-frame layout

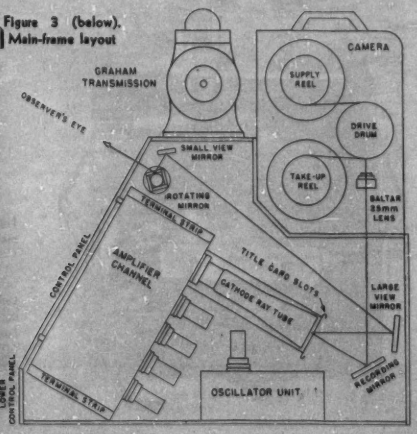

Figure 4. Rear view of front panel

(a) Calibration end channel; (b) beam-control channel; (c) amplifier channels; (d) connecting wires and lugs

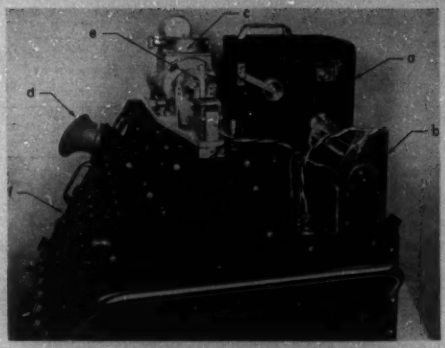

Figure 6. Side view of main recording unit

(a) Camera; (b) mechanical timer for drum operation; (c) drive motor and Graham variable-speed transmission; (d) port for viewing tube spots; (e) manual clutch control; (f) front panel and channel controls

The amplifiers and end channels are each complete in a 5 by 10 by 2 inch chassis unit. The manner in which the amplifiers and end channels are mounted on the front panel and in the main unit is also indicated in Figure 3. The end channels are connected to the amplifier units by means of wires attached to lugs on both ends of the amplifiers and end channels, Figures 3 and 4. The amplifier-channel connections are made in this manner so that the replacement of a faulty channel may be easily achieved.

Figure 5 shows a front view of the main recording unit with the front panel, and the chassis mounted thereon, removed. The permalloy cathode-ray tube support acts as a shield against magnetic fields. The flash lamps are used for photographing title cards. These cards, regular 3 by 5 inch index cards on which record titles have been written are mounted in a slot in front of the cathode-ray tube faces, Figure 3. A capacitor is then discharged through the flash lamps and the cards are photographed, thus providing a permanent title on each record.

Operation

There are essentially two modes of operation of the moving-film camera. In continuous operation 100 feet of film are available for recording at film speeds of 1/3 to 12 feet per second. In drum operation, 10 inches of film are available for recording at speeds of 1/3 to 38 feet per second. The ease with which the 10-inch strips of film may be processed makes it desirable to use drum operation wherever possible. However, the choice of continuous or drum operation depends on these factors: the film speed necessary to resolve the signal; the duration of the signal; and the feasibility of synchronizing the signal with the recording period of the oscillograph. With drum operation the total recording period at the slowest film speed is $2^1/_2$ seconds; at the fastest film speed it is only 20 milliseconds. With continuous operation the recording period may last up to five minutes.

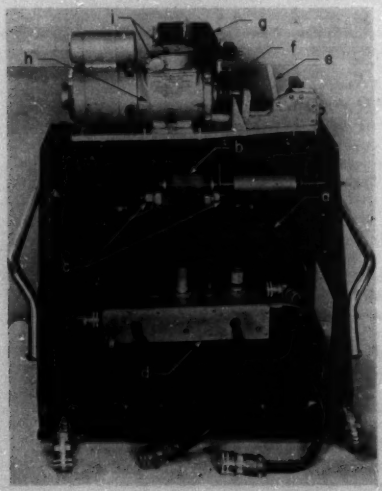

Figure 5. Front view of main recording unit with front panel removed

(a) Permalloy support for cathode-ray tubes; (b) rotating mirror; (c) flash lamps; (d) oscillator chassis; (e) manual clutch control; (f) clutch; (g) camera; (h) motor and Graham transmission drive; (i) film-speed dial and control

The mechanical timer, Figure 6, was developed for use with drum operation of the camera to aid in the synchronization of the recording interval and the signal. An integral part of the timer is a switch which may be used to initiate the phenomena to be studied. This initiation may be made to occur at any reasonable time during or before the period of recording.

Often there are short-duration signals to be recorded that cannot be synchronized as indicated before, and another method of synchronization is necessary if drum operation is to be used. Auxiliary equipment is now being developed at the Naval Ordnance Laboratory which automatically synchronizes the recording

Figure 7. Sample oscillogram

This oscillogram shows the usefulness of the instrument as used for both sequence-of-events timing and quantitative pressure measurements. Trace A is the 1,000-cycles-per-second square-wave timing signal. The second timing trace is not shown. Trace B is an amplified version of trace C to show the rise time; trace D is an amplified version of trace E. Trace F shows the primer flash signal, the only part of the oscillogram that has been retouched, and the time recoil begins. Traces C, E, and G are the output of strain-gauge pressure gauges fed directly into the 6TCRO while traces B and D have an additional stage of amplification

period with the occurrence of the signal. Th signal to be recorded is used to switch on the beams while the signal is also delayed electrically and introduced into the amplifiers after the beams are centered.

Conclusion

The six-trace cathode-ray oscillograph has been in use at the Naval Ordnance Laboratory for about two years, and has seen a great variety of uses during this period. The high-frequency response has made possible the recording of short-duration phenomena of the order of 200 microseconds, such as the impact of a steel hammer on a steel anvil, and the pressure-time curves of underwater explosions. The absence of phase and amplitude distortion in the low frequencies, as is present in a-c amplifiers, has made possible the faithful reproduction of long-duration transients. The high sensitivity of the channels has made possible the recording of the output of strain gauge circuits without additional amplifiers. The instrument has also been very useful in the determination of the time sequence of various complicated phenomena. Figure 7 is a sample oscillogram.

The high sensitivity, good-frequency response, high-input impedance, and convenience of operation has led to a demand for the instruments for a variety of uses both by the various sections within the Laboratory and by other government laboratories.

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2. Reference 1, page 400.

No Discussion

A Precision Electrothermic Voltmeter for Measurements Between 20 and 20,000 Cycles

F. L. HERMACH
ASSOCIATE AIEE

AN INSTRUMENT has been developed at the National Bureau of Standards primarily for standardizing voltmeters over a wide voltage range at audio frequencies. It is an electrothermic voltmeter of the thermocouple type, with an adjustable resistor in series with an insulated-heater, evacuated thermoelement. It is intended as a transfer standard, with its indications referred to a standard cell by means of a d-c potentiometer. In conjunction with a suitable potentiometer it can be used for measuring alternating voltages from 0.4 to 400 volts, at frequencies ranging from 20 to 20,000 cycles per second with an accuracy of 0.01 per cent or better.

The Transfer Principle

The basic electrical units are established at national standardizing laboratories in terms of the units of a concordant mechanical system, by means of experiments which are used to assign values to groups of standard cells and resistors. With such standards and by means of the potentiometer and its accessories, measurements of direct voltage, current, and power are made. The measurement of the corresponding a-c quantities depends primarily on certain types of electrical instruments which ideally have the same response on alternating current, for the defined a-c units, as on direct current. Such instruments, when calibrated with direct current and then used on alternating current, become standards which, in effect, transfer the units from direct to alternating current. Electrodynamic transfer instruments have been highly developed at the National Bureau of Standards for precise measurements and for standardizing other instruments at power-line frequencies,[1,2,3] and corresponding electrostatic instruments have been developed at the National Physical Laboratory.[5,6]

If an instrument is arranged to permit frequent calibrations on direct current, long-time stability or freedom from drift, low temperature influence, low losses, and other normally desirable characteristics become of secondary importance and the instrument may be designed and constructed to secure the best possible frequency characteristic. Such a transfer instrument need not have a scale in the ordinary sense at all, and may be designed for an increased sensitivity of reading by methods that would not be applicable to instruments intended for general use.

Description of the Voltmeter

Several types of instruments were considered for laboratory use as a transfer voltmeter at audio frequencies, and a thermocouple instrument, with a thermoelement in series with an adjustable resistor, was chosen as being most suitable over the necessary voltage range. The frequency limitations of such an instrument were carefully studied. All known sources of error were analyzed, and the magnitudes of the a-c–d-c differences which might result were at least roughly computed. Then, to guard against the possibility of appreciable errors from unsuspected causes, the instrument was tested by comparing its a-c–d-c performance with that of other types of instruments which might serve as transfer standards.

A voltmeter composed of a resistor in series with a thermoelement can be correctly used as a transfer instrument over the range of frequencies for which the output electromotive force of the thermoelement is constant for a constant applied voltage. This will be true if the response characteristic of the thermoelement, $E = f(I)$, and the effective impedance, $Z = V/I$, are each constant over the frequency range, where I is the rms current through the heater and V the rms voltage applied to the combination. The response characteristic of the type of thermoelement used in this voltmeter was believed to be suitably constant for frequencies well into the megacycle range, and the low-frequency and d-c response were verified by special tests which are described in the next section.

In order that the effective impedance of the voltmeter be independent of frequency, it is in effect necessary that no appreciable fraction of the current entering the heater be diverted through the thermocouple circuit. For the voltage-measuring device connected to the thermocouple in this instrument, it would be difficult to assure this with direct-connected thermoelements, in which the heater and thermocouple are electrically connected. Fortunately, suitable thermoelements in which the heater and thermocouple circuits are electrically but not thermally insulated by a small ceramic bead are available in milliampere ranges. Thermoelements may be constructed in which the capacitance between the two circuits is less than 1 micromicrofarad, insuring negligible capacitance currents through the apparatus connected to the thermocouple, at frequencies far above the range planned for this instrument. Such an evacuated thermoelement, with a single junction of the thermocouple in

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F. L. HERMACH is with the National Bureau of Standards, Washington, D. C.

thermal contact with a straight-wire heater, is used in this instrument.

An effective time constant of 1×10^{-7} or less and a skin effect of less than 0.01 per cent are necessary for the circuit from the points of voltage measurement through the resistor and the heater of the thermoelement, to insure constancy of the effective impedance within 0.01 per cent at frequencies up to 20,000 cycles per second. The values of time constant and skin effect for the straight-wire heaters of the type of thermoelement used in this instrument are well below this, so that the upper frequency limit is set by the series resistor. A commercially-available, shielded, audio-frequency decade resistor, modified to have two decades of 1,000 ohms per step and one decade each of 100, 10, and 1 ohms per step, all in series, is used in this voltmeter. The time constant of the circuit, computed from values of the equivalent series inductance and direct shunt capacitance of the resistor, is less than 5×10^{-8} at 20,000 cycles per second for each setting of each decade. The skin effect is also negligible for each decade. The adjustable series resistor is connected as a 3-terminal resistor, as shown in Figure 1, since for this connection the errors caused by the capacitances to the shield, which might otherwise amount to 0.1 per cent if the shield were tied to a resistor terminal, are considerably reduced. For example, calculations of the effects of the distributed capacitance of the resistance windings to the shield and of the capacitance of the studs and their connecting wires to the shield showed that by this connection the change in impedance over the range would be reduced by a factor of about four and would cause an error of less than 0.005 per cent at 20,000 cycles per second. The rotary switch brush of each decade is connected to the next decade of higher resistance, so that for the 3-terminal connection the current through the brush-to-shield capacitance of the highest decade is merely a load on the source. Additional computations showed that the brush-to-shield currents of the lower decades, including the middle 1,000 ohm-per-step decade, would cause errors of less than 0.002 per cent for switch capacitances as great as 10 micromicrofarads.

This study indicated that for each setting of the dials the effective impedance would remain constant within 0.01 per cent or less for frequencies up to 20,000 cycles per second. The actual frequency limit is somewhat beyond this upper limit at which this voltmeter will be used, and is set by the shunt capacitance of the highest decades. This shunt capacitance is apparently largely in the switch, and made it impossible to use a 10,000-ohm-per-step decade in this resistor. Such a decade would otherwise be desirable, and may well be feasible by special design.

A 30-milliampere evacuated thermoelement was chosen for this instrument, and was mounted in a phenolic enclosure at the end of the resistance box. A reference level of heater current of 20 milliamperes for which the electromotive force of the thermoelement is 6 millivolts, was selected. This is comfortably within the 24-milliampere limit of the highest decades of the resistor, and the maximum power dissipation of eight watts at the rated voltage of 400 volts is not considered excessive.

For convenience of reading and for portability, the indicating element in the usual thermocouple voltmeter is ordinarily a pivoted d-c instrument. Although satisfactory for most purposes, such an instrument would not have the required sensitivity for this application. Millivolt potentiometers have long been used with thermocouples in the measurement of temperature. However, since the voltmeter is used solely as a transfer instrument, the output electromotive force of the thermocouple need not actually be measured. Thus a potentiometer, while convenient, is not absolutely necessary, and for this instrument a rudimentary potentiometric circuit of Poggendorf's second or constant-resistance type,[4] which has since become known as a Lindeck element, was constructed. With this circuit, the greater portion of the electromotive force of the thermocouple is balanced out by the voltage drop in a 10-ohm resistor, leaving only a small voltage difference to be read on the "partial scale" of the galvanometer which completes the circuit. This arrangement in effect constitutes an elementary form of deflection potentiometer.

In use the series resistor of the voltmeter is adjusted to keep the heater current near a selected reference level so that the partial scale can be calibrated directly in per cent of the total indication. This is very convenient for the measurement of small voltage differences, as in a transfer test of another instrument. The scale is calibrated by observing the indications for two successively applied direct voltages differing by a small percentage, as measured with a potentiometer connected in parallel with the instrument. Such calibrations need be made rather infrequently. For an alternating-voltage measurement, as in an a-c test of another voltmeter, the series resistor is adjusted for any convenient indication on the partial scale and the direct voltage for the same indication is then measured with a d-c potentiometer.

The Lindeck element was primarily designed to have adequate stability. A number 6 dry cell is used as the source and two commercially-available helically-wound adjustable resistors of 100 and 5,000 ohms permit adjustment of the balancing current (0.6 milliampere) within 0.001 per cent. Repeated tests showed that contact resistance variations are negligible and that the changes in the voltage drop across the 10-ohm manganin resistor are less than 0.02 per cent during an hour.

A reflecting, wall-mounted galvanometer with a straight, linear scale, and a special thermofree key,[4] complete the thermocouple circuit. For the small differences (up to 1 per cent) normally encountered in a transfer test, the approximations involved in the use of such a scale, in place of a nearly quadratic scale to fit the response of the thermoelement, can easily be justified. No precautions other than the use of the special key were taken to guard against changing extraneous thermal electromotive forces. The

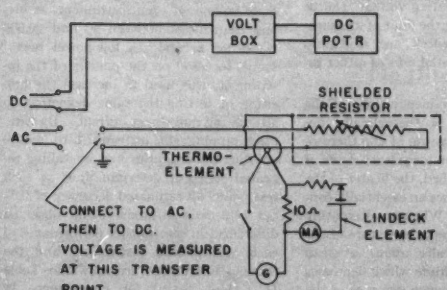

Figure 1. Schematic diagram of voltmeter circuits

sensitivity of the voltmeter depends, of course, on the characteristics of the galvanometer, and amounts to about 1 centimeter change in deflection for a 0.1 per cent change in applied voltage.

Tests of the Voltmeter

The first tests of the completed instrument, made with direct voltage, verified that ample sensitivity could readily be obtained and that the drifts were negligible for the types of thermoelements and for the circuits used. After a warm-up period of a minute, the changes in the indication of the instrument did not exceed 0.01 per cent in 15 minutes, and indicated that no extensive thermal precautions would be necessary under usual laboratory conditions.

Early measurements had indicated that with direct current through the heater, the electromotive forces of all thermoelements tested were dependent to a slight extent upon the direction of current flow. This was true even for insulated thermoelements of the type used in this voltmeter, in which no heater current flows through the thermocouple. Differences in electromotive force ranging from less than a hundredth up to a few tenths of a per cent have been observed for the two directions of the same magnitude of current flow on direct current. Tests verified with high precision that these differences caused no appreciable a-c–d-c errors provided the mean of the indications for the two directions of current flow was used as the d-c reference. The tests were made by connecting a pair of thermoelements in series and measuring their comparative a-c–d-c differences at a convenient audio frequency. When the mean of indications on direct current was used as the reference, the comparative a-c–d-c differences were in general less than 0.001 per cent, even for those pairs for which one thermoelement had a small and the other a large reversal difference. Exceptions were noted only when one particular thermoelement, which had a difference on reversal of about 0.2 per cent was used in the measurements. The tests together with the results of the tests described in the following paragraphs, indicated that this element had an a-c–d-c difference of about 0.05 per cent, which would make it unsuitable for use in this voltmeter. The measurements indicated the desirability of testing each individual thermoelement in this manner before using it for precise work with direct current, for the a-c–d-c difference in this thermoelement was wholly unexpected. With this exception, however, the measurements indicated that in general, for insulated-heater thermoelements, no appreciable transfer differences should be expected even if reversal differences are considerable.

A laboratory-type, suppressed-zero, electrodynamic voltmeter,[1] designed for precise measurements at power-line frequencies, was used to verify the low-frequency performance of the thermal voltmeter by direct comparison of the a-c–d-c differences of the two voltmeters at 30 and at 60 cycles per second. The tests were made by connecting both instruments in parallel, properly setting the series resistor of each, then adjusting the voltage for the same deflection of the electrodynamic voltmeter on alternating voltage, direct voltage, and direct voltage in the opposite direction, and observing the corresponding indications of the thermal voltmeter. The settings were made in quick succession in the order indicated to eliminate the effects of any possible residual drifts in either instrument. From the averages of the readings of the thermal voltmeter on alternating voltage and on direct voltage the comparative a-c–d-c difference of the two voltmeters was computed. At least two such series of observations were made at each test point and the averaged results were corrected for the computed a-c–d-c differences caused by the measured inductance of the electrodynamic voltmeter. The residual differences in the transfer performance of the two instruments are shown in Table I. For convenience, each observed value is tabulated as the per cent difference in applied voltage necessary for the same deflection of the electrodynamic instrument on direct current and on alternating current at the indicated frequency, on the assumption that the thermal instrument was without transfer error. The residual differences were all less than 0.005 per cent of the measured voltages at these frequencies. Because of erratic fluctuations in the alternating-voltage source used, this was nearly the limit of measurement. The differences are negligibly small for all anticipated uses of either instrument.

The actual low-frequency limit of the thermal voltmeter is theoretically determined by the response of the thermoelement itself. When a constant value of direct current is applied, the heater of the thermoelement reaches an essentially constant temperature. When an alternating current is applied, the temperature of the heater varies cyclically about a mean value, with an amplitude which decreases with increasing frequency, because of the thermal capacity of the element. At frequencies high enough so that the cyclic variations are negligible, the temperature rise of the heater is independent of frequency, if skin effect is unappreciable. However, at lower frequencies the mean value of temperature on alternating current may not be the same, because of the departure from Newton's law of cooling and the change of heater resistance with temperature. Errors may thus be expected at very low frequencies. The tests showed these to be entirely negligible at power-line frequencies for the thermoelement used in this voltmeter, and the actual low-frequency limit at which errors become appreciable is very probably considerably lower. It is hoped to determine this by difference tests of thermoelements having very different thermal capacities. The effect is mentioned here to show that for electrothermic as well as for electrodynamic and electrostatic instruments, which are also used on direct current, there is a gap in satisfactory response at low frequencies which can be narrowed but not closed.

Table I. Comparisons Between the Electrothermic and the Electrodynamic Voltmeters

Frequency, Cps	Voltage, Volts	Observed Differences, Per Cent of Indication
30	120	+0.002
60	20	+0.004
	50	+0.005
	150	+0.003
	240	+0.002

Two portable electrostatic voltmeters having ranges of 150 and 300 volts respectively were compared with the thermal voltmeter over the full range of frequencies at these voltages. They were new pointer and scale-type instruments of exceptionally good design and construction, with scale lengths of over five inches. A-c–d-c difference tests similar to those already described were made at the full-scale voltage of each instrument, at several frequencies between 30 and 20,000 cycles per second. A low-power microscope, focussed on the pointer of the instrument, was used to increase the precision of setting the same deflection on alternating and direct current. By carefully tapping the instrument to reduce pointing uncertainties and by taking repeated series of determinations at each test point, an estimated accuracy of 0.01 per cent was attained. The observed differences in the transfer performance of each electrostatic instrument and the thermal instrument are shown in Table II. Here again, for convenience in

Table II. Comparisons Between the Electrothermic Voltmeter and Two Portable Electrostatic Voltmeters

Frequency, Cps	Observed Differences in Per Cent of Indication	
	150-Volt Instrument	300-Volt Instrument
30	−0.004	−0.004
100	+0.002	+0.004
300	+0.005	
1,000	−0.004	+0.010
3,000	+0.006	
10,000	+0.004	
15,000		+0.008
20,000	+0.002	−0.009

visualizing the results, each observed value is tabulated as the per cent difference in applied voltage necessary for the same deflection of the electrostatic instrument on direct current and on alternating current at the indicated frequency on the assumption that the thermal instrument was without transfer error. From this table it is evident that the differences were 0.01 per cent or less.

Standardizing Voltmeters

This voltmeter is used at the National Bureau of Standards primarily for standardizing a-c voltmeters at audio frequencies, including power-line frequencies. Two distinct types of such tests, transfer and a-c tests, are made.

Transfer tests serve to determine the frequency characteristic or frequency influence, referred to a base or reference frequency, of instruments which accurately respond to a range of frequencies. A-c–d-c difference tests of other instruments are the most important tests of this kind, but determinations are also made occasionally with 60 cycles per second as the reference frequency, for instruments which do not accurately respond to direct voltage. The tests are made essentially as described in the previous section. By taking observations in the order indicated all effects such as scale errors, temperature influence, drifts, and so forth, which are the same at both frequencies are eliminated, and only those which are frequency-dependent are observed. These are relatively permanent for a given instrument so that a single test of this nature is adequate and future tests need be made only at the reference frequency.

Straight a-c tests are usually made for instruments which accurately respond to only a narrow range of frequencies (as for example uncompensated moving-iron instruments for use at 60 cycles per second). They amount to, in effect, determinations of the voltage necessary to deflect the instrument to each of several scale points. Any future tests to determine the extent of subsequent drifts in the instrument must also be made at this frequency. Such tests are in general less convenient than tests with direct current, and give less information than a transfer test combined with subsequent d-c tests. Therefore the transfer test is usually more valuable than a straight a-c test for instruments which can be used with full accuracy on direct current.

Conclusions

The development and tests of an electrothermic transfer voltmeter have been described, and its application to voltmeter testing has been outlined. This instrument can be used with a suitable d-c potentiometer to measure alternating voltage at audio and power frequencies to an accuracy of 0.01 per cent or better. The transfer principle, upon which the use of this instrument depends, has been re-emphasized. The voltmeter shows excellent transfer performance, comparable over a wide range of frequencies to that attainable with the best laboratory-type electrodynamic instruments at power-line frequencies.

It may be constructed with comparatively inexpensive, commercially-available components. Its excellent performance indicates, therefore, the perhaps unsuspected potentialities of electrothermic instruments of this type.

The voltmeter is, of course, a special instrument, not generally suitable for ordinary direct measurements of voltage. Except for transfer testing, its use is somewhat slow and cumbersome, and requires a potentiometer and its auxiliaries. However, such a potentiometer is a practical necessity in any laboratory in which precise measurements of current, voltage and power are to be made. The same potentiometer could be used in place of the simple Lindeck element described in this paper, if it has a suitable millivolt range, so that in many laboratories only a good decade resistor and thermoelement need be provided in order to permit in addition the precise measurement of alternating voltages. Alternatively, small portable millivolt potentiometers with internal galvanometers of ample sensitivity are now available, and could be used with the series resistor and thermoelement to make a convenient, entirely portable transfer standard.

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No Discussion

Performance of Portable Electrical Instruments in Magnetic Fields

A. E. PETERSON
NONMEMBER AIEE

Synopsis: A knowledge of the performance of electrical instruments in the presence of magnetic fields is of importance to their intelligent use. The present paper describes: a study of this problem as applied to portable voltmeters, ammeters and wattmeters; and a brief survey of such magnetic fields as might be found around certain laboratory or small-plant equipment. Only portable direct- and 60-cycle a-c instruments were studied; panel- and switchboard-type instruments, presenting problems of their own were not included.

Experimental Setup

Helmholtz Coils

FOR producing the required field about the test instrument, a pair of Helmholtz coils one meter in diameter, shown in Figure 1, was constructed. Each of the two coils contained two 55-turn windings of number 13 American wire gauge insulated-copper wire. With all turns in series aiding, this gave a 2-gauss field at the axis with a current of one ampere.

The coils were mounted on gimbals which allowed the axis to be swung to any desired azimuth and elevation. The instrument under test was set on a rotatable table in the middle of the coils, the table assembly being supported through the gimbal trunnions. By an arrangement of pulleys and drive cables, the gimbal-mounted coils could be rotated to various positions while the instrument table remained fixed in its original orientation in space. In this way the instrument was maintained in a fixed position with respect to the earth's field. Care was taken in construction to avoid the possibility of short-circuited turns in the bronze control cables.

No magnetic materials were used in the assembly. The only metallic parts were the windings, control cables for the table, and a pair of slip rings. All other parts were of plywood, masonite or bakelite, dowelled and glued together.

Measurements of alternating fields set up in the coil indicated the field to be uniform within three per cent in the volume later occupied by the largest of the instruments tested. This was a fifteen inch by fifteen inch by five inch "laboratory standard" voltmeter. The field measured at the center of the coil assembly checked the design computations within 3 per cent.

Metering and Control Equipment

Each instrument tested was placed in the field of the Helmholtz coils and its readings were compared with a shielded reference instrument placed well away from this field. To insure that the quantity measured in the test instrument would remain unchanged, the pointer of the reference instrument was set to coincide with the cross-wires of a fixed-position microscope. The test instrument was read with a hand magnifier.

For d-c work, the field coils and the instruments were supplied from separate batteries and controlled by suitable rheostats and potential dividers.

A-c studies required more elaborate equipment, in that it was necessary to adjust the phase of the field with respect to the current in the test instrument. For wattmeters, a separate phase adjustment was used to regulate the power factor shown by the wattmeter. For a-c observations, a motor-generator set was used having two 3-phase generators coupled to the same motor shaft. The stator of one generator could be rotated with respect to the other, giving one of the required phase adjustments. For the other phase adjustment, a small phase-shifting transformer was used as needed. Remotely controlled rheostats in the two generator field circuits, variable-ratio autotransformers and fixed-ratio transformers were used to adjust the voltages and currents. The phase angle between the Helmholtz coil field and the current in the instrument under test was determined by use of a wattmeter. The current in one circuit of the wattmeter was in phase with the current in the Helmholtz coils, the current in the other wattmeter circuit was in phase with the current of the instrument under test.

Methods of Field Measurement

Alternating fields were measured by placing a small calibrated search coil in the field and reading the pick-up voltage with an electronic voltmeter. The field was then computed from the formula

$$B = E/(\omega A N \times 10^{-8})$$

where

B = field in gausses, rms
E = coil voltage, volts, rms
A = effective coil area in cm^2
N = number of turns in the coil
$\omega = 2\pi \times$ frequency

The AN constant for the coil used was a little over 15,600, giving about one volt for a 17-gauss 60-cycle field.

The strength of direct magnetic fields and of rms alternating fields was assumed to be numerically equivalent for equal direct and rms alternating currents.

For measuring other direct fields about the laboratory a small commercial gauss meter was used; its sensitivity was two gausses per division, with one division equal to about two millimeters. These measurements were of lower accuracy than those on alternating current, but were adequate for their purpose—establishment of the approximate magnitudes.

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A. E. Peterson is with the National Bureau of Standards, Washington, D. C.

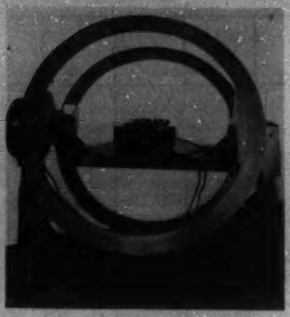

Figure 1. Helmholtz coil assembly

Effects of Magnetic Fields on Various Types of Portable Electrical Instruments

By the use of the Helmholtz coil and its auxiliary equipment, described in the section on Experimental Setup, representative portable instruments of the types available in the electrical instruments section's laboratory of the National Bureau of Standards were tested for their performance in magnetic fields.

Selection of Fields for Study

Three field magnitudes were chosen for the tests: 1, 5 and 10 gausses. The first was used as being typical of the field to which an instrument might be exposed under ordinary laboratory conditions or where, in the vicinity of stronger fields, reasonable care was taken in its use. The 5-gauss field is that usually mentioned in manufacturers' literature relative to the shielding of their instruments. The 10-gauss field, an admittedly severe test, was used to study the effectiveness of the shielding of the better instruments; it did result in rather absurd readings for some of the unshielded ones.

Moving-iron a-c instruments were tested in a-c fields. Permanent-magnet moving-coil meters were tested in d-c fields. The work on electrodynamic instruments was confined to alternating currents and voltages and a-c fields. (Shielded instruments of the latter type display hysteresis effects on direct current the magnitudes of which depend on the previous magnetization of the shield. To describe these effects it would be necessary to state completely the previous magnetic history of the shield. This has not been attempted in the present paper. One generally corrects for such effects by taking the mean of reversed readings.)

Test Procedure

Each instrument was tested in the horizontal position.

A preliminary survey was first made to find the field directions for maximum and minimum effects. The field was then directed to produce the greatest effect; when indicated, the instrument was oriented also so that the earth's field would have its least effect. On a-c tests, the phase angle between the field and the instrument supplies was adjusted for maximum effect.

Each instrument was checked in zero-, 1-, 5-, and 10-gauss fields at low, middle and high scale points. Additional measurements in 2-gauss fields were made in some cases. For each condition, observations were made for reversed field effects. Half of the difference between the observed scale divisions—expressed in per cent of full scale—was taken as the influence of the field upon the instrument. Depending on the type of instrument, reversal effects were secured in one of three ways: by reversal of lead connections to the test instrument; by 180 degree rotation of the Helmholtz coils; or by reversal of the current through the Helmholtz coils.

Results of Tests

Figures 2, 3, and 4 give the basic results for the three types of instruments tested. The plotted points refer to the upper end of the scales. The field influence is expressed in per cent of full scale as defined in the preceding paragraph. The statements of accuracy shown on the curves were secured from manufacturers' literature. The stated scale lengths are approximate.

It should be noted that the curves of Figures 2, 3, and 4 refer to values taken near the upper end of the scale. Further data for lower parts of the scale may be summarized as follows:

1. For moving-iron instruments the influence of the external field, expressed in per cent of full scale, remained essentially constant over the scale. This would be anticipated from their "square law" response.

2. As might be expected, for permanent-magnet moving-coil instruments the effects, for a given field, were proportional to angular deflection, that is the current in the moving coil. Thus a field having a one per cent influence at full scale value had, very closely, a one-half per cent influence at half scale.

3. Electrodynamic instruments, especially wattmeters, yielded less consistent results than the foregoing two types. Since electrodynamic voltmeters and ammeters have a "square law" response similar to that for moving-iron instruments, it might be expected that they would be influenced by external fields as in (1) above; results with these instruments were not entirely consistent but were generally similar to those for moving-iron instruments. Wattmeters

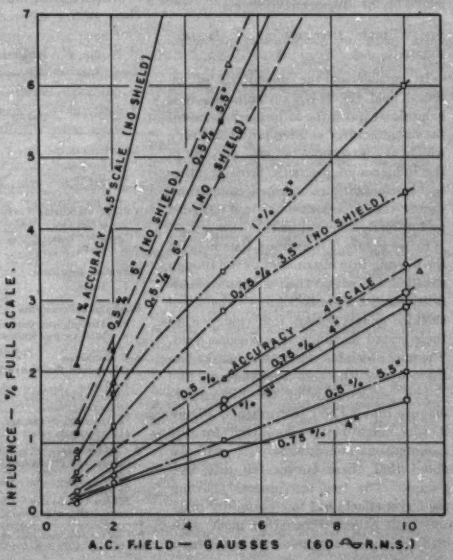

Figure 2. Effect of magnetic fields on moving-iron instruments

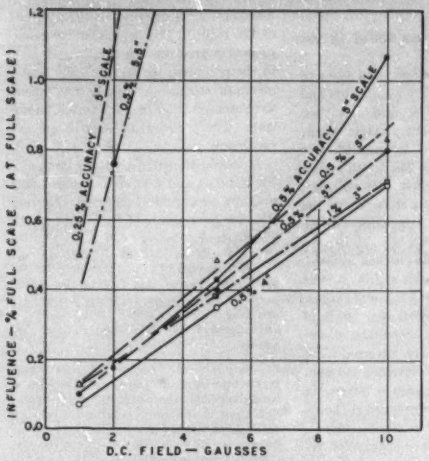

Figure 3. Effect of magnetic fields on permanent-magnet moving-coil instruments

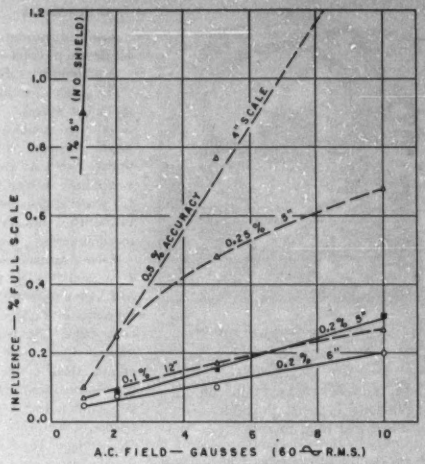

Figure 4. Effect of magnetic fields on electrodynamic instruments

gave rather inconsistent results at the lower parts of the scale. It was expected that the influence of the field would depend on the magnitude of the current in the moving coil and on its orientation relative to the applied field; this simple relation presumably was modified by the effect of the shields on the internal field distribution, and by the variation in angular size of scale division as determined by the instrument construction.

Stray-Field Influence on Instruments

In considering the magnitude of external field effects on instruments, it is helpful to express the influence in terms of the manufacturer's stated initial accuracy which, of course, does not take account of the influence of magnetic fields or other ambient conditions. Such a comparison shows the superiority of well-designed and properly shielded instruments.

From Figure 2 it appears that none of the moving-iron instruments, even though shielded, fall within their accuracy classifications in a 5-gauss field. Of the seven shielded meters shown, four came within their stated accuracy in a 2-gauss field. The unshielded instruments are marked on the graphs; the rest are shielded.

The performance of the permanent-magnet moving-coil instruments, Figure 3, was much better than that for the moving-iron instruments. It will be noted that these curves fall into two groups. The two instruments showing the larger effects had low-coercivity magnets; the others were either made with high-coercivity magnets or had heavy shields. The influence of a 5-gauss field was less than the manufacturers' stated accuracies for the latter instruments.

Electrodynamic instruments, Figure 4, were intermediate between the other two types. Of the six tested, the influence of a 5-gauss field was less than the accuracy figure for two instruments. Three of the others were within, or very nearly so, their stated accuracies in 2- to 4-gauss fields. The one unshielded instrument (so marked) showed up very poorly, of course, under this sort of test.

Use of Instrument to Detect Stray Fields

If its supply is sufficiently steady the instrument itself may be used to check

Table I. Maximum Measured Fields Near Electric Equipment

Equipment	Field in Gausses — Distance from Equipment, Centimeters: 5	10	20	30
Adjustable-Ratio Auto Transformer				
1. 0.5 kva	2.4	0.7	0.2	
2. 2 kva		1		
3. 7 kva*		18	17	4
Voltage-Stabilizing Transformer				
4. 0.100 kva	1.4	0.6	0.15	
5. 0.250 kva	0.3	0.2	0.1	
6. 1 kva		8.8	1.5	0.4
Induction Regulator				
7. 1 kva		0.4	0.15	
Electrical Instruments				
8. Moving iron voltmeter (unshielded)	0.2	0.06		
9. Moving iron ammeter (unshielded)	0.2	0.04		
A-C Machines (Measured Fields Are Rms Alternating Current)				
1/30 hp	0.2	0.07		
1/6 hp	0.5	0.1		
10 hp induction	1.5	0.6		
75 hp synchronous	4.5	2	0.8	0.2
415 hp synchronous	5		0.5	0.1
D-C Machines (Measured Fields Are Direct Current)				
20 hp	10	3		
75 hp	20			5
275 kw (Generator)	10		4	
(Synchronous Converter)			10	

* This transformer was mounted immediately beneath a wooden bench top with the control wheel above the bench. Instruments on the bench would be subjected to the fields shown.

the presence of harmful stray fields. This may be done by noting any change in readings of the instrument for positions 180 degrees apart, for a number of orientations. Except for permanent-magnet moving-coil instruments (and for moving-iron a-c instruments in d-c fields) such a check may be made also by reversal of the leads to the instrument. It is best, of course, to locate the instrument outside of any disturbing field. If this cannot be done completely, one may locate an orientation so that a 180 degree change in position causes a minimum change in reading. This assumes that the external field will not change direction at a later time.

Typical Magnetic Fields About the Laboratory

Table I gives the results of a survey of stray fields around various "suspect" items used in the National Bureau of Standards. The tabulated values represent the maximum fields observed at the given distances; the various distances for a given item were not necessarily in the same direction nor were the orientations of the fields necessarily the same. The distances were measured to the nearest point on the case or frame of the item under test. Readily movable pieces were so located as to minimize the effects of nearby magnetic materials. The larger items were tested as they stood, though in some cases their stray fields were obviously distorted by nearby structural steel and steel panels.

Comments on Table I

Placing a portable instrument in the fields tabulated for the 5 centimeter distance would amount practically to placing the instrument in contact with the apparatus case or frame. This is because the internal working parts of an instrument are, on the average, within that distance from the outside of its case.

Of the apparatus surveyed, only the 7-kva adjustable-ratio autotransformer (item 3), the 1-kva voltage-stabilizing transformer (item 6) and some of the larger rotating machines showed fields as large as one gauss at distances of 10 centimeters (4 inches) or greater.

The observed stray fields were found to drop off very rapidly with distance from the source, although this might not be the case for similar equipment near magnetic materials, such as steel supports and panels. If the presence and source of stray fields are realized, portable instruments can often be placed so as to be in very weak fields. Means of roughly checking the presence of such fields are indicated in the section on Use of Instruments to Detect Stray Fields.

The conditions of use of item 3 in Table I—the 7-kva adjustable-ratio autotransformer—were of some interest. As mentioned in the note (Table I) it was mounted directly beneath a wood bench top, with its control wheel on top of the bench. Auxiliary portable instruments would ordinarily be placed on the bench top and would be in strong magnetic fields. (The pointer indication of an unshielded moving-iron voltmeter, with 135 volts applied, varied from 120 to 150 volts as the instrument was moved about on the bench top.) The strength of the stray field at a given point varied greatly with the condition of loading and the resultant current and amount of the winding in circuit.

It should be recognized that the foregoing survey is merely illustrative and may not be at all typical of stray fields encountered around other types of apparatus and in other places, particularly near conductors carrying large currents.

Conclusions

1. Permanent-magnet moving-coil instruments which have high-coercivity magnets, or which are well shielded, are usually not influenced by a 5-gauss field to an extent greater than the manufacturer's stated accuracy.

2. Shielded electrodynamic instruments generally were influenced, in a 5-gauss field, to an extent somewhat greater than their stated accuracies but were within this figure in 2- to 4-gauss fields.

3. All the moving-iron a-c instruments tested showed effects, in 5-gauss fields, greater than their stated accuracies. Most of the shielded ones were within these figures in a 1-gauss field.

4. Of the available equipment checked only the larger adjustable-ratio autotransformers, certain voltage-regulating transformers, and the larger rotating machines showed external fields that should be expected to affect nearby electric instruments.

5. Judged by fields found near equipment in the author's laboratory, it is to be expected that the fields encountered in ordinary applications of portable instruments generally will be considerably less than five gausses.

Discussion

H. B. Brooks (Consulting Electrical Engineer, Washington, D. C.): The author's paper is a timely one, for we need to be reminded of the possibility of errors caused by stray fields, sometimes from unsuspected sources. For example, a very troublesome case of discrepancy in measurements was finally traced to the stray field from a slide rheostat in which the wire was wound on an enameled steel tube. The variable-ratio voltage-regulating autotransformer, which has deservedly found widespread adoption, should not be placed close to instruments, particularly to those of unshielded types.

The author gives values of field strength at distances of 5 and 10 centimeters from moving-iron instruments. Inasmuch as two of the older classical type of unshielded portable permanent-magnet moving-coil instruments will affect each other's accuracy by nearly one per cent when side by side, and by nearly two per cent at the minimum separation with pole pieces facing each other, it would be of interest to know the corresponding figures for modern d-c instruments with high-coercivity magnets, shielded and unshielded, in the possible combinations, for several relative positions and for various separations.

A. E. Peterson: The tests outlined below were made to extend Brooks' original observations[1] to permanent-magnet moving-coil instruments of more recent design.

Five instruments were intercompared: *a* and *b* were unshielded instruments of the older type having magnets of low coercivity; *c* and *d* were shielded instruments of more recent design, having low or medium coercivity magnets; *e* was a shielded instrument having a high coercivity magnet.

The following results were secured for full-scale deflections with the instruments at minimum separation and pole-pieces facing each other:

The effect of *a* on *b* for such orientation amounted to 1.8 per cent.

Pairing *a* successively with *c*, *d* and *e* gave effects ranging from 0.1 to 0.3 per cent. The effect of *c* on *a* was approximately the same as of *a* on *c*, and similarly for the other pairs.

The effects of *c*, *d* and *e* upon each other, when similarly checked by pairs, amounted in every case to less than 0.05 per cent.

Exploration around the foregoing instruments with a small gauss meter showed some interesting results. A field of about 30 gausses was measured close to the case of instrument *a* adjacent to the pole pieces; this dropped to 5 gausses at about an 8 centimeter distance from the case. Fields around instruments *c*, *d* and *e* were less than 1 gauss, about the limit to which the gauss meter could be read.

Brooks' comments about the stray fields of tubular rheostats wound on steel tubes suggested their comparison with rheostats of nonmagnetic construction which previous tests had showed to have very small fields.

One rheostat, wound upon an enameled steel tube, and having 29 ampere-turns per centimeter at rated current, showed fields of about 6 gausses at 10 centimeters from the ends and from the sides. Another rheostat, identical with the other except that it was wound on a porcelain tube, showed only 0.2 gauss fields at similar locations. A third rheostat, wound on a nonmagnetic tube, but having a steel slider-bar and a steel tie-rod through its center, showed approximately 1-gauss fields at the comparable points.

Reference

1. The Accuracy of Commercial Electrical Measurements, H. B. Brooks. AIEE Transactions, volume 39, 1920, pages 117–8.

Silicone Insulation in Submarines—Toxicity

H. P. WALKER
NONMEMBER AIEE

T. E. SHEA, JR.
NONMEMBER AIEE

Synopsis: The effects of temperature and operation time upon the production of noxious gases from silicone insulated electrical equipment under conditions of simulated submarine service are reviewed and some of the general problems of toxicity of burning insulation are discussed.

A previous paper[1] discussed the subject of silicone insulation as applied to Naval electric power equipment and concluded that one of the problems yet to be worked out was the toxic effect of the products of decomposition, if any, of silicone materials used for motor insulation. In this paper, the authors will present the results of tests recently completed on this subject, as well as discuss the general problem of burning insulation.

Shipboard electric installations present special problems for insulating materials that may not exist in shore installations. As Naval vessels are necessarily built to a high degree of compartmentation requiring forced ventilation and also have large capacity power plants installed in relatively small spaces, there is perhaps more insulation per cubic foot of available engine room space than exists in land installations. Many Naval vessels are complete cities in themselves. An electrical fire in a surface ship is extremely dangerous while an electrical fire in a submarine is a hazard. For this reason, all electrical insulating materials used in Naval vessels are at present carefully screened to determine their suitability at elevated temperatures and the effects of noxious gases produced on thermal decomposition of these materials.

All organic insulation (and some of the inorganics), when subjected to temperatures above their "curing" temperatures, produce volatiles, some of which may be toxic, and the degree of toxicity depends on the amount of material decomposed, the temperature of the heat source, the conditions of air circulation and supply, and the length of time exposed to that temperature.

Many tests have been conducted to determine the toxicity of laminated and molded insulation used commercially, as well as by the Navy.[2,3] The Bureau of Ships sponsored a number of these tests and others were conducted by commercial organizations. The objectives of these tests, procedures and results are too voluminous to discuss in this paper; however, they do prove that carbon monoxide was the most dangerous decomposition product present.

In order to determine the effects of temperature and operation time upon the production of noxious gasses from silicone insulated rotating electric equipment under conditions of simulated submarine service, the Bureau of Ships requested the Bureau of Medicine and Surgery to set up a test program to determine these conditions. This project was assigned to the Naval Medical Research Institute in August 1946 and was completed in December 1947.[4] Briefly, the test procedures were set up as shown in the following paragraphs.

Navy type class *A* insulated 3-horsepower 440-volt alternating current 3-phase squirrel-cage induction motors were selected for test purposes as representative types of motors for Naval service and because of their ease of handling. These motors were stripped and rewound with silicone insulating materials so as to give maximum temperatures.

All motors were tested in a high-pressure chamber located at the Naval Medical Research Institute. This chamber, containing 190 cubic feet of space, was gas-tight, and was provided with means of controlling the ambient temperature without introducing or withdrawing air during the test, thus closely simulating a submerged submarine.

Motors to be tested were secured to a motor test block inside the chamber. They drove a nonsilicone insulated d-c generator, by two pulley belts (Figure 1). The motors were powered from the 440-volt supply to the Institute. Motor loading was accomplished by controlling resistance banks located outside the chamber. Currents developed by both the motor and the generator were indicated on ammeters located outside of the chamber, and frequent readings, made at regular intervals, were recorded; voltages developed by the generator were similarly recorded.

Iron-constantan thermocouples, located as indicated in Figure 1, were used to record all test temperatures on a temperature indicating recorder (0 to 300 degrees centigrade range).

In giving motor specifications, it is customary to indicate the number of degrees rise above ambient temperature to be expected. However, since preliminary tests indicated that the production of toxic gases by silicone insulation is related to the true temperature rather than to the number of degrees rise, all motor temperatures are actual winding temperatures as recorded on thermocouple number 1. These winding temperatures are the observable "hottest-spot" temperatures (temperature rise by thermocouple plus ambient temperature).

Samples of chamber air, for analysis, were withdrawn at the start and the end of each test and at 24-hour intervals during the run. White rats were used in all tests except the final one in which two

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H. P. Walker is with the Bureau of Ships, United States Naval Department, Washington, D. C., and T. E. Shea, Jr. is a Lieutenant Commander on the staff of the Naval Medical Research Institute, National Naval Medical Center, Bethesda, Md.

human volunteers were used. For each test eight normal rats were exposed, two to a cage, with adequate food and water (Figure 1). At the conclusion of each test, the rats were bled as soon as possible by cardiac puncture and the blood samples were analyzed for carboxyhemoglobin (COHb) in volumes per cent. Per cent saturation with COHb was calculated on the assumption that the CO capacity of rat blood is 21 volumes per cent. This assumption is supported by a measure of the hemoglobin content of normal rat blood. It is to be stressed that while the values for rat COHb were of toxicological significance, they were not to be regarded as a quantitative measure of the effect on human beings.

At the start of each test the chamber was sealed and placed under a pressure equal to 40 inches of water.

A total of seven (7) motors were tested at temperatures ranging from 50 to 300+ degrees centigrade (top burnout temperatures were believed to approach 650 degrees centigrade). Most test runs were continued for a period of 96 hours; however, several tests were terminated at the end of 72 hours due to various mechanical difficulties. The motors were rewound with silicone insulating materials by the Westinghouse Electric Corporation, the General Electric Company, and the Dow Corning Corporation, using silicone products as produced by the latter two organizations.

Test Results

From the results of the foregoing tests as reported by the Naval Medical Research Institute[4] and from the observations of the authors during the tests, the following points are significant:

1. The generator used for loading the motor never exceeded class *A* insulation temperatures, so it may be concluded that this machine did not contribute to the evolved gasses in the test chamber.

2. At no time during the tests did the oxygen content of the chamber fall below a safe limit, nor did the carbon dioxide content approach a harmful value.

3. Preliminary tests of silicone insulating materials showed that the noxious substances given off on thermal decomposition were carbon monoxide, carbon dioxide, phenol and formaldehyde.

4. The motor tests revealed that carbon monoxide was the only substance found in the chamber in medically significant concentrations. Its concentration varied as a function of motor temperature and time duration of the test. Terminal values of 110 ± 50 parts per million CO in air and 12 ± 5 per cent COHb in the blood are to be expected when motor temperatures (observable hot spot temperatures) range between 200 and 230 degrees centigrade. The complete test data are reported in Tables I and II and plotted in the curve shown by Figure 2.

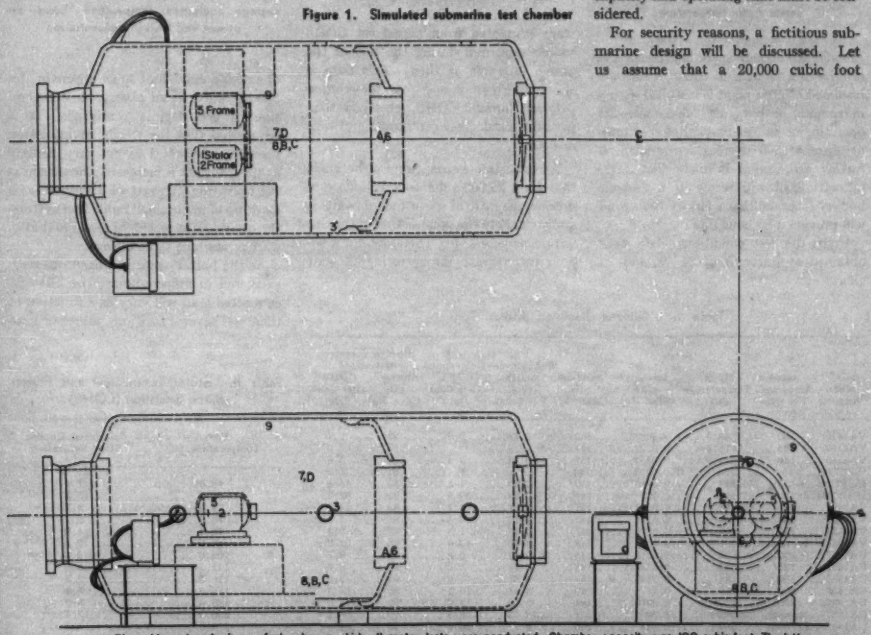
5. Insulation temperatures in excess of 300 degrees centigrade, maintained for even short periods of time, constitute a hazard to health.

Discussion

Before the data reported by the Naval Medical Research Institute can be applied to the design of an electric plant for shipboard use, the factors of installed kilowatt capacity and operating time must be considered.

For security reasons, a fictitious submarine design will be discussed. Let us assume that a 20,000 cubic foot

Figure 1. Simulated submarine test chamber



Plan, side and end views of chamber in which all motor tests were conducted. Chamber capacity was 190 cubic feet. The letters represent the position where rats were exposed in cages. The numbers represent position of thermocouples: 1. Test motor insulation, thermocouple wound-in. 2. Test motor frame. 3. Ambient air. 5. D.C. Generator frame. 6. Thermocouple at rat cage A. 7. Thermocouple at rat cage D. 8. Thermocouple at rat cages B and C 9. Ambient air. A. Two rats exposed in lock. B and C. Four rats exposed in cages on deck of main chamber. D. Two rats exposed in cage hung from overhead.

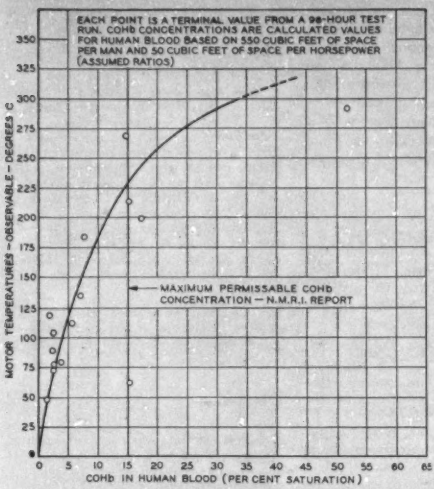


Figure 2. Carbon monoxide concentration versus motor temperature

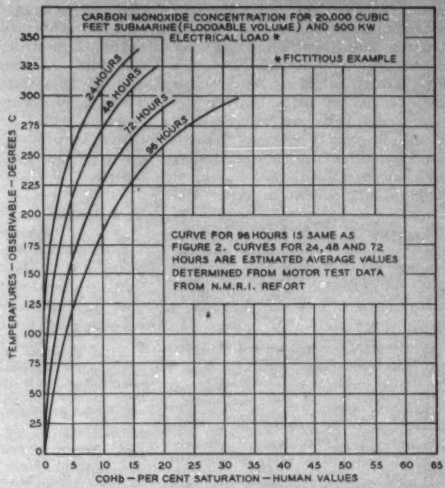


Figure 3. Relation between observable average equipment temperature, hours exposure and COHb concentration

(floodable volume) space represents the interior of a submarine. Also, a silicone-insulated electric plant is installed with a maximum output of 2,500 kilowatt operating at an observable hot spot temperature of 200 degrees centigrade. A further assumption is made that 2,500 kilowatt load will result in a 24-hour battery rate, and that a 500-kilowatt load will provide a 96 hour rate.

Under the test conditions, data were obtained at intervals of 24, 48, and 72 hours, as well as 96 hours, as shown in Table I. Human COHb concentrations were calculated from actual rat COHb values measured during the test at the above intervals of time. This data is shown in Figure 3, and gives the relation between human COHb, observed temperature in degrees centigrade and time in hours.

A reasonable assumption can be made that for a 20,000 cubic foot volume submarine, the ratio of space and kilowatts is approximately the same as that of the test setup; namely 1:1. Therefore, for a 96-hour battery rate, the operating kilowatt of a 20,000 cubic foot space approximates 500 kilowatts at an observable temperature of 200 degrees centigrade. It is recognized that the weight of insulation used per kilowatt does not vary directly as the kilowatt is increased; however, as the operating kilowatt of a submarine is made up of many small individual motors, the above ratio of 1:1 of space and kilowatt is assumed to be correct.

As the battery rate is a fixed quantity expressed in ampere-hours, the kilowatt connected load will vary as a function of time. If several fictitious values of kilo-

Table I. Silicone Insulated Motor Tests

Motor Number	Rated Amperage, Per Cent	Motor Temperature (C)	Ambient pCO (Mm. Hg)	Ambient pO_2 (Mm. Hg)	Rat Values—Determined COHb (Vol. Per Cent)	Rat Values—Determined COHb (Per Cent Sat.)	Human Values—Calculated Alveolar pO_2 (Mm. Hg)	Human Values—Calculated COHb (Per Cent Sat.)
VA4410	100	48.6	0.00663	130	0.19	0.90	83	1.4
VA4410	125	78.4	0.0223	141			93	4.0
VA4410	150	111.5	0.0330	157	0.72	3.4	106	5.8
1S3B7284	100	134	0.0433	128			81	7.2
1S3B7284	125	213	0.0940	136	2.00	9.5	87	15.4
1S3B7284	150	291	0.533	140	6.29	29.9	92	52.2
VA4427	100	62	0.0937	141	1.97	9.4	88	15.3
VA4427	125	89	0.0125	141	0.16	0.8	88	2.5
VA4427	150	119	0.0122	147	0.27	1.3	93	2.3
VA4427	burnout	300+	0.604	143	8.40	40.0	90	55.2
UA1957	100	65	0.0110	145	0.19	0.9	92	2.1
UA1957	125	76	0.0139	146	0.21	1.0	93	2.7
UA1957	150	104	0.0147	144	0.37	1.8	92	2.8
UA1957	200	200	0.117	140	3.21	15.3	88	17.5
UA1957	220	270	0.0919	144	2.65	12.6	92	15.0
UA1957	burnout	300+	1.700	152	12.55	59.8	99	66.2
UA1957	burnout	300+	0.348	150	4.71	22.4	98	34.2
VA4239	100	72.4	0.0140	143	0.21	1.0	90	2.7
VA4239	180	183	0.0471	145	1.17	5.6	92	8.0

Note: Results of exposure of white rats to chamber gas and calculated results for human beings. The figures given represent terminal values for tests of approximately 96 hours duration. Each value for rat COHb is an average from eight animals.

Table II. Motor Temperature and Human Blood Saturation (COHb)

Motor Temperature, (C)	Calculated Human Blood Saturation
48.6	1.4
62.0	15.3
72.4	2.7
76.0	2.7
78.4	4.0
89.0	2.5
104.0	2.8
111.5	5.8
119.0	2.3
134.0	7.2
183.0	8.0
200.0	17.5
213.0	15.4
270.0	15.0
291.0	52.2

Calculated blood values are based on exposed personnel under conditions of simulated submarine operation.

Table III. Toxicity Concentrations—20,000 Cubic Feet Submarine

Load in Kilowatts	Operating Time in Hours	Estimated COHb Concentration in Per Cent 150 C	175 C	200 C	225 C	250 C	Insulation Weight Factor
500	96	7.0	9.0	11.5	14.5	18	1
1,000	72	8.0	12.0	15	12.0	25	2
2,000	48	8.0	12.0	18	24.0	30	4
2,500	24	3.5	6.0	10	16	22.5	5

Notes: 1. COHb values for 500 kw taken from Figure 2 are for 96 hours.

2. For all practical purposes the weight of insulation for 1,000 kw, 2,000 kw and 2,500 kw is the same as for 500 kw; both propulsion and auxiliary rotating equipment being considered. However, assuming that double the kilowatts will double the weight of insulation, the COHb values will be as shown.

3. COHb values for 500 kw at 24, 48, and 72 hours are taken from Figure 3.

watt load and time are selected, it is possible to determine the COHb concentration over a given temperature range as shown by Table III.

Conclusions

It may be concluded that properly cured silicone-insulated rotating equipment installed in submarines when operating at observable temperatures not exceeding 230 degrees centigrade continuous temperature does not constitute a health hazard up to 96 hours operating time submerged.

Insulation temperatures of the total installed rotating electric plant in excess of 300 degrees centigrade, maintained for even short periods of time, constitute a hazard to health; however, a single motor "burnout" in a given submarine installation is not believed likely to increase the total level of CO concentration to a dangerous value.

Surface ship installations of silicone-insulated electric equipment, where adequate ventilation and air replacement are maintained, are considered to be safe from the standpoint of CO evolutions regardless of temperature or "burnout" condition.

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No Discussion

Functional Evaluation of Insulating Materials

K. N. MATHES
MEMBER AIEE

DESIGN of modern electric apparatus is placing increasing requirements on insulating materials. The demand for materials to withstand higher temperatures, higher speeds and other extreme design requirements is continually increasing. Moreover, insulating materials must now withstand more severe climatic conditions from arctic cold to tropical heat and moisture. Airborne electric equipment must withstand extremely rapid changes in ambient temperature and humidity.

Recent trends toward decreased size, weight, and cost have greatly increased the insulation problems. New materials with radically different properties involve new application problems. Methods of evaluating insulating materials should produce significant conclusions in terms of design and service requirements. It is the purpose of this article to consider the type of methods now in use and to describe ways in which these methods are being improved.

Evaluation work carried on in the past, and now in progress, takes many forms but usually will fit one of the following categories:

Type of Test	Quality Evaluated
Test on materials (varnish, mica, plastics, and so forth)	Properties of individual materials
Tests on component parts (coils, bars, commutators, and so forth)	Properties of combinations of insulating materials
Tests on complete apparatus	Simulated service
Service records	Actual service

These categories should be interrelated for maximum benefit. For example, tests on component parts may contribute to recognition of a need for tests of specific materials. Likewise, tests on individual materials may be used to limit the number of tests on component parts or complete machines. Insulation evaluation programs which include work in all four categories are most productive when each phase is designed to aid the understanding and significance of the others. Experience with such programs indicates that a functional approach must always be emphasized.

This may be relatively easy in tests on complete apparatus, but tests on component parts sometimes tend to lose significance and tests on materials may become so arbitrary that they are useless in determining the performance of insulating materials in actual apparatus.

Service Records

The most significant evaluation of insulating materials should, of course, be those obtained from service records. However, failures in service are often difficult to interpret since the damage may be so great that the evidence is destroyed. On the other hand, a temporary failure may leave so little damage that the failure cannot be located. Wherever possible, the following items should be incorporated in the failure reports:

1. Age, rating, type of machine.
2. Duty cycle—continuous, intermittent, and so forth.
3. Maximum temperature obtained in the winding or a temperature log if available.
4. Chronological log of insulation-resistance measurements and associated winding temperature. Measurements should be made at at least two different intervals after applying voltage—for example 1 minute, and 10 minutes.
5. Type of winding and location of fault.
6. Appearance of fault and insulation with pictures if possible. Color transparencies afford more contrast and detail than black and white pictures.
7. Amount of oil, dirt, linters, or other materials on surface of insulation and nature of attack on insulation, if any.
8. Unusual climatic or ambient conditions such as excessive heat, high relative humidity, severe dust storms, chemicals, iron dust, or other solid contaminants.
9. Unusual conditions preceding failure such as lightning storms, short-circuited lines or switching operations which might produce surges, or any other severe or unusual electrical condition.

Service records are most valuable when collected systematically over a period of years. But at best, service records are limited and are likely to suffer from incomplete data, lack of control, and chance failures. Nevertheless, service records can guide the more easily controlled tests on complete apparatus.

Tests on Complete Apparatus

Next to service records, tests on complete apparatus can give the most accurate indication of the performance of insulating materials. The apparatus can be run under various duty cycles and can be exposed before or during such cycles to various laboratory conditions, such as high-ambient temperatures, high-relative humidity, moisture condensation, salt spray or fog, water immersion and even blasts of dirt, iron dust, and so forth. Failure to operate usually constitutes the end point of such tests but it is customary to measure insulation resistance also.

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K. N. Mathes is with the General Electric Company, Schenectady, N. Y.

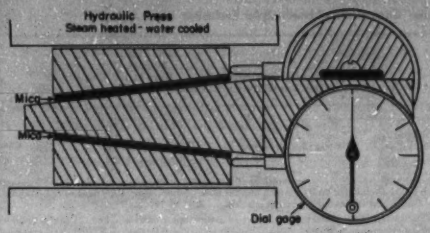


Figure 1 (above). Wedge designed to apply both shear and compressive stress to bonded mica sheet

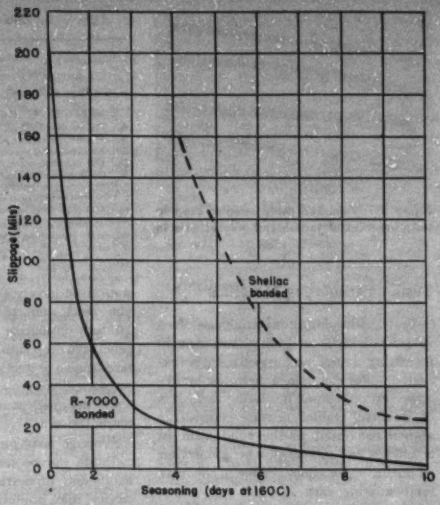


Figure 2. Slippage of two bonded mica samples in the wedge test

Voltage withstand and surge tests can sometime be used advantageously.

It may prove difficult to set up adequate or sufficient combinations of test conditions to meet the wide variety of service requirements. The size of the following list for only a few possible combinations serves to illustrate this problem.

1. Heat aging alone.
2. Moisture exposure alone.
3. Salt spray or fog exposure alone.
4. Heat aging plus moisture exposure.
5. Heat aging plus salt fog exposure.
6. Moisture exposure after contamination with dirt.
7. Salt fog exposure after contamination with dirt.
8. Heat aging plus moisture exposure after contamination with dirt.
9. Heat aging plus salt fog exposure after contamination with dirt.

Obviously, it is possible to select only a few of these test conditions. This selection should be based on types of conditions most likely to be encountered in service and the conditions under which the insulation is most likely to fail. Consideration should be given also to the effect of accelerated test conditions. In illustration, an extensive study of the effect of increasing temperature on heat aging of 1/4 horsepower motors has been made by Scott and Thompson.[1] This study illustrates also the statistical variations obtained in such programs. A minimum of 80 identical motors were needed to evaluate just one type of insulation system. Only heat aging was studied. The effect of moisture or other conditions would have required many more motors.

Tests on complete machines are of necessity restricted in scope or limited to small inexpensive apparatus. For large apparatus, tests on component parts such as coils may prove necessary.

Tests on Component Parts

Tests on component parts can be made under the same variety of test conditions as described for complete apparatus except that running conditions may be difficult to simulate. For example, it may be easy to apply a voltage between winding and ground and yet very difficult to obtain voltage between turns in the absence of the magnetic circuit. Mechanical factors such as vibration, centrifugal force, and so forth, are particularly difficult to simulate. Despite these restrictions, very useful and significant information can be obtained from tests on component parts.

Of course, many types and varieties of component parts are in use, however, and a comprehensive study of each with all of the possible types of insulating materials would be a long process and probably impracticable. Hence, the evaluation of the insulating materials themselves becomes necessary.

Table I. Comparison of Methods for Evaluating Moisture Resistance

Type of Varnish	Moisture* Permeability	Dielectric Strength** Volts Per Mil on Film At 50 Per Cent Relative Humidity 25° C	After 24 Hours in Water at 25° C	Immersed† Rod, Time to Failure	Magnet Wire Cable—U‡ Time to Failure Number of Varnish Dips 2	4	6	7
Oil modified asphalt	0.4×10^{-8}	600–1,400	300–400	1/2–2 hours	23 hours	21 hours	48 hours	
Red pigmented short oil alkyd	0.8×10^{-8}	1,000	210	1/2–4 hours	5 minutes	20 hours	24 hours	29 hours
Oil modified phenolic	4.8×10^{-8}	1,800	1,280	30 days	5 minutes	10 minutes	30 hours	30 hours
Oil modified alkyd	2.2×10^{-8}	1,220	700	1/2–2 hours	5 minutes	5 minutes	5 minutes	30 minutes

*Grams per hour per centimeter per millimeter of mercury. ASTM-D-697-42—method A.

**Two coats of varnish each 0.001-inch thick on 0.005 in copper panel. ASTM-D-115-46.

†0.59 inch (1.5 centimeter) brass rod with three coats of varnish each 0.001-inch thick. Navy Specification 52V13.

‡7-strand 0.032-inch Formex wire cable bent into U shape with 1/2-inch radius and varnish treated with varnish at recommended viscosity. Number of dips indicated in heading.

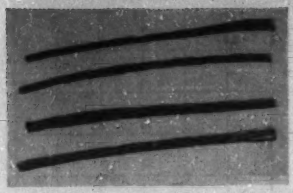


Figure 3. Varnished cable samples used to test properties of varnish and magnet wire in combination

Table II. Comparison of Methods for Evaluating Heat Aging

Type of Varnish	1/8-Inch Mandrel Flexibility* Days at 150°C to Failure	Film Rupture—Varnished, Magnet Wire Cable** Days at 160°C to Failure: Formex	Cotton	Glass
A. Oil modified alkyd	30	90	144	260
B. Pigmented, short oil alkyd	1/2	80	45	>180
C. Unmodified phenolic	Will not film on copper	110	14	180
D. Oil modified phenolic	2	55	30	75
E. Oil modified asphalt	1-2	21	30	60

* Two coats of varnish each 0.001-inch thick on 0.005-inch copper panel, 1-inch wide strips bent over 1/8-inch mandrel after heat aging. End point indicated by visual cracking. ASTM-D115-46T.

** 7-strand 0.032-inch magnet wire cable deflected at center of 3-inch span until cracked. Cracks determined by measuring insulation resistance in water. Elongation in outer strand calculated from deformation at failure.

Tests on Insulating Materials

Tests on insulating materials are often considered to be of little significance in indicating actual performance to be expected. For example, a dielectric breakdown test on a liquid insulating varnish is of doubtful value, because varnish is always converted to the solid form in actual use and solvents are largely driven off during application. Some of the tests for insulating varnishes, are just minor variations of tests developed previously for surface finishes. In such tests, a thin film of varnish is formed on a thin copper panel (replacing the steel or wood panels used in the tests on finishes) and tested in this form, even though insulating varnish is practically never used as a thin film on metal but rather as binder, saturant or finish for other types of insulating materials. So long as insulating varnishes were used largely as surface finishes, the film tests were to some extent useful. Modern insulating varnishes, however, penetrate deep into the insulation structure to give protection throughout and to act as a binder for the structure. Film tests are useless in evaluating these characteristics.

Slippage tests on mica illustrate how methods can be used to evaluate really significant properties. In commutator service, mica insulation is subject to shear as well as compressive stress at operating temperatures. Pasted mica sheet for this purpose is tested conventionally between two metal plates under straight compression. In order to include the factor of shear stress, the simple wedge shown in Figure 1 was built. In Figure 2, the slippage of mica pasted with two different binders is compared as a function of seasoning time. The significant difference shown between these two binders has been confirmed by service experience.

Tests on Composite Samples

In order to bridge the gap between tests on materials, and tests on component parts, an additional method of test is being investigated. In this work, samples are made from combinations of materials, magnet wire and varnish, for example. Such samples need not look like any component part actually used in equipment but should follow the principles outlined below:

1. Insulating materials should be combined in a manner similar to the combination found in actual apparatus.
2. Samples should be simple, easily made and reproducible.
3. Samples should be designed in such a fashion that properties important in manufacture or use can be easily evaluated, quantitatively, if possible.
4. One basic form of sample should be adaptable to as many types of test as possible.

Magnet wire treated with varnish is an important part of the insulation system of most electric apparatus. The varnished magnet wire cable sample illustrates a composite sample developed to evaluate the combination of varnish and magnet wire. To make this sample, magnet wire—cotton, glass fiber, enamel insulated is machine stranded to a concentric 7-

Figure 4. Flexibility as a function of heat aging

Comparison of Cable Samples Treated with Varnishes:

Varnish A—oil modified alkyd
Varnish B—pigmented short oil modified alkyd
Varnish C—unmodified phenolic
Varnish D—oil modified phenolic
Varnish E—oil modified asphalt

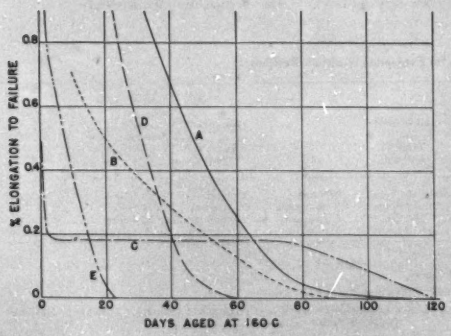
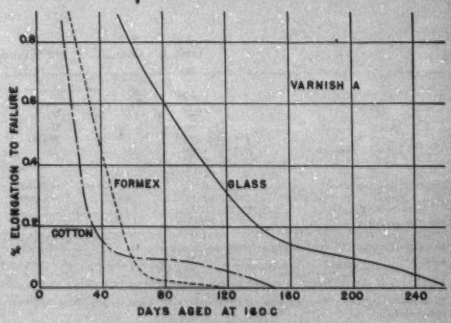


Figure 5. Flexibility as a function of heat aging

Comparison of different magnet wire insulation treated with an oil modified alkyd varnish



strand cable (with 0.032-inch wire a 2-inch pitch is used). This cable is cut to length, varnish treated, and baked as needed. In order to represent deep coil sections, the cable may be taped with aluminum foil which prevents varnish penetration along the side but permits it at the cut end of the sample. Several varnished cable samples are pictured in Figure 3. Such samples can be used for a variety of tests listed below:

1. Rate of varnish cure (insulation resistance).

2. Solvent or varnish attack on enamel (insulation resistance).

3. Film continuity (dielectric breakdown after water immersion).

4. Varnish penetration (visual inspection).

5. Rigidity and cure (plastic deformation and creep).

6. Heat aging (elongation to film rupture).

Several of these tests have been discussed in a previous article.[1] To illustrate the advantages of tests on composite structures, two tests with the magnet wire cable can be discussed.

Film Continuity

Varnish treatment is intended to prevent the entrance of moisture and dirt within the varnished structure. Three types of tests have been used to indicate the suitability of varnishes in this respect.

1. Wet dielectric strength (varnish filmed copper panel).

2. Moisture permeability (varnish film).

3. Brass rod immersion test (time for failure with 125 volts of direct current applied between varnish-coated rod and salt or acid solution).

In these three tests, every effort is made to obtain a continuous film or the test is invalidated. In practice, however, a smooth film is difficult to obtain because of the rough nature of the underlying insulation surface. The ability of a varnish to film such a surface may be more significant in preventing the entrance of moisture or dirt than the properties measured by any of the three tests listed.

The cable sample (bent into a U and varnished) can be immersed in salt or acid water and tested in much the same way as the brass rod sample. In this instance, however, the small multiple insulated wire surface is characteristic of the surface of many types of apparatus. Such surfaces are much more difficult to film than smooth metal surfaces. In fact, from two to eight varnish dips may be needed to give film continuity with the cable sample depending upon the characteristics of the varnish. The filming characteristics of a varnish do not seem to correlate at all with dielectric breakdown or moisture permeability as shown in Table I. It is probable, that in the majority of applications, the film-forming properties have greater significance. Both the pigmented alkyd and the asphalt varnish have superior film forming properties as shown in Table I with the cable U test. Service experience with such pigmented alkyd and oil modified asphalt varnishes has been particularly good where exposed to moisture even though for these varnishes, the moisture transmissivity is relatively high and the wet dielectric breakdown is low. These tests confirm the principle that a sample using insulating materials in combination should be used to permit significant evaluation.

Heat Aging

When exposed to high temperatures for long periods of time, varnished structures become embrittled. The progress of this degradation is one of the most interesting and important characteristics of insulation. The mandrel flexibility test has been used to compare heat aging characteristics of varnishes. In this method, a 0.001-inch varnish film on a 0.005-inch copper panel is bent about a 1/8-inch mandrel after increasing periods of time at an elevated temperature (usually 125 degrees centigrade) until the film cracks. The varnished magnet wire cable sample can be used also to evaluate heat aging. A 4-inch long heat aged sample is supported on a 3-inch spacing and the center is deformed in increments until the structure cracks. Results for Formex samples treated with various varnishes are shown in Figure 4. The rate at which elongation (flexibility) decreases varies greatly with different varnish treatments. For example, the unmodified phenolic varnish treated sample (*c*) becomes somewhat rigid quite quickly but for a long period thereafter does not become more rigid. In contrast the other varnishes which are initially more flexible may with time become more brittle than the unmodified phenolic varnish.

In Figure 5, heat aging of magnet wire cable samples, made of Formex and cotton and glass-insulated wire, treated with an oil modified alkyd varnish is compared. With a different varnish, a different order of comparison may be obtained (that is, cotton may be very inferior, and so forth). The considerable effect of the wire insulation emphasizes again the importance of evaluating insulating materials in combination. Table II gives additional comparisons for various varnishes and compares these results to mandrel flexibility tests on varnish coated copper panels. The complete lack of correlation is apparent. Service experience again seems to correlate more with the evaluation of the magnet wire-varnish combination rather than with the tests on materials alone.

Conclusions

Methods of evaluating insulating materials and systems should have the following features:

1. It should be possible to interpret results so as to aid design or predict service performance.

2. A majority or at least a principal number of the factors involved in design or application should be included.

Many of the methods now used, particularly in the evaluation of insulating materials, cannot meet such a test. Two practical approaches to the problem can be suggested.

1. Tests on insulating materials or systems should be made up with specific design or service problems in mind.

2. Insulating materials often should be evaluated in simple combinations, characteristic of the way in which they will actually be used.

It is apparent that insulation evaluation will become more varied and complex if evaluation is to be more useful and significant. The need for this increased complexity may indicate inadequacy and limitations of existing material specifications.

Care should be used that this increased complexity of insulation evaluation tests does not increase the burden of material specifications, which at present often limit, unnecessarily and undesirably, the design engineer in the choice of materials. These material specifications are best written so as to control or identify materials as manufactured or purchased.

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Discussion

G. L. Moses (Westinghouse Electric Corporation, East Pittsburgh, Pa.): Mathes is to be complimented on his objective approach to this very important problem in evaluating insulating materials. I heartily concur with his philosophy. It is quite important that we distinguish between tests which evaluate the performance characteristics of insulations and those tests which are intended merely as identity checks. It is therefore of great importance that the selection and application of materials be based on tests which evaluate operating conditions in so far as possible in the laboratory. This problem has been recognized for some time and numerous approaches have been studied. It has been our observation that a test specimen which is suitable for one particular variety of apparatus may not be equally suitable for another. Therefore, we select the variety of insulation composite sample for studying the particular insulation performance characteristics.

It is highly desirable that the user and the manufacturer of electric apparatus see eye to eye with respect to the testing of insulating materials as a basis for their application to electric apparatus.

K. N. Mathes: We agree with Moses that a test specimen which is suitable for one particular variety of apparatus may not be equally suitable for another. However, every effort is made to develop samples which have as broad an application as possible.

This often can be accomplished by keeping in mind the fundamental factors involved without attempting to duplicate the geometry of a particular application.

We appreciate Moses' emphasis on the desirability of the approach described in our work.

Magnetic Amplifiers for Naval Shipboard Applications

L. W. BUECHLER
MEMBER AIEE

ONLY the most reliable of equipment can be tolerated in a Naval vessel's electric system. Since the ship's gun controls, radar, and communication are all dependent on the ship's electric system the only thing a ship can do in case of failure of this system is retreat. It certainly could not take any active part in fighting action, in fact, a crippled ship often requires protective assistance from other ships, just when the other ships could be used most effectively elsewhere in the battle.

Keeping the ship's propulsion system operable is of prime importance if the ship is to survive. If it fails the ship becomes dead in the water and is easy prey for an enemy's guns, torpedoes, or bombs. Another very important system is the steering. Should the steering system fail the ship must stop, or run uncontrolled, usually in circles. Although great effort is devoted to making a ship's propulsion and steering not totally dependent on the electric system, often even these important systems are dependent on the ship's electric system. All large naval vessels, such as troop transports, cargo vessels, as well as combatant ships, have certain weapons, such as antiaircraft guns, which must be kept in operation, and all vessels have communication systems which are essential to their efficient operation. One of the most important of all special considerations in the design of reliable shipboard electric equipment is that of shockproofness. The mechanical shock (that is, sudden violent movement of the ship's hull and internal equipment) caused by the firing of a ship's own guns and the exploding of enemy rockets, shells, mines, and torpedoes, frequently causes extensive damage to many types of equipment.

Vacuum tubes always have been classified as a very undesirable component in a system as important as the ship's electric system. Great strides have been made in improving the ruggedness of vacuum tubes but, notwithstanding these improvements, vacuum tubes still fail too often. Contractors supplying the electric equipment often have found it difficult to accomplish various functions in equipment without using vacuum tubes. For example, in a recently developed automatic steering control, four amplifiers were required. To improve the reliability of the system, a spare set of vacuum tube amplifiers with changeover switches was provided. Frequently a loss of accuracy or performance of an item, such as a voltage regulator, has had to be permitted in order to avoid the use of vacuum tubes.

Advantages of Magnetic Amplifiers

It was late in the last war that it became evident that magnetic amplifiers could be used to good advantage in many places where vacuum tubes were previously thought to be necessary. The vacuum tube may be considered as a variable resistance in a d-c circuit; whereas, the magnetic amplifier may be considered as a variable impedance in an a-c circuit. In either case a small amount of control power is used to vary the current or power in the power circuit. Magnetic amplifiers consist basically of saturable reactors and dry-disk rectifiers and can readily be made much more rugged and reliable than vacuum tubes. They already have been used in some applications and have been found to be very reliable and maintenance-free. It is desired and believed obtainable to have units which, once installed, will not require any routine service or maintenance during a service life of at least five years.

In addition to being very rugged and maintenance-free, magnetic amplifiers are considered to offer the following advantages:

1. They can operate from the ship's a-c supply and, for most applications, require no power supply such as the "B" supply required for vacuum tubes. Although the reactors used in the magnetic amplifier may weigh more than a comparable vacuum tube, the size and weight of a complete magnetic amplifier can be made equal to or less than the vacuum tube amplifier since the "B" power supply required with the vacuum tube amplifier is usually a large portion of the total size and weight.

2. They require no warm-up time. Components, such as gun controls, would not have to be kept continuously energized just in case they suddenly might be required.

3. They do not have the power limitations of vacuum tubes. Due to the heat loss in vacuum tubes the power output, without special cooling of the tubes and chassis, is often a limiting feature in designs. The greatest heat loss problem in magnetic amplifiers is in the rectifier. Present work indicates that rectifier efficiencies of 85 to 90 per cent are possible; the heat loss for 1- to 2-kw of output is not serious.

4. Very high power amplification per stage is possible. A power gain, that is ratio of controlled power in load to power in control circuit, of 1,000,000 to 1 or even 100,000,000 to 1 per stage is possible but not considered practical for most applications due to slow response which may range from one to five seconds. For most applications in which a response of 1/200th to 1/10th of a second is desired, a power gain of 1,000 to 1 to 10,000 to 1 per stage is considered practical. An input control signal of from one milliwatt to a few microwatts is considered sufficient to control the first stage of amplification. Smaller control signals have been used but are not considered developed to the stage where they can be used generally.

5. The time of response can be made to meet most requirements. The time of response usually is measured in cycles of time referred to the a-c power supply. For

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L. W. Buechler is with the Bureau of Ships, United States Navy Department, Washington, D. C.

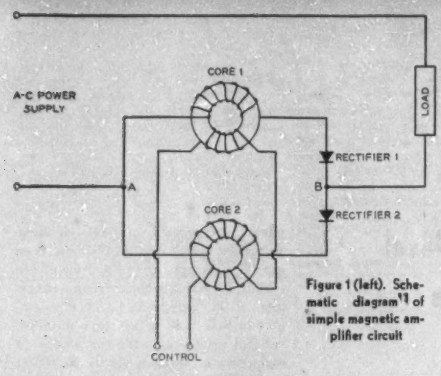


Figure 1 (left). Schematic diagram[11] of simple magnetic amplifier circuit

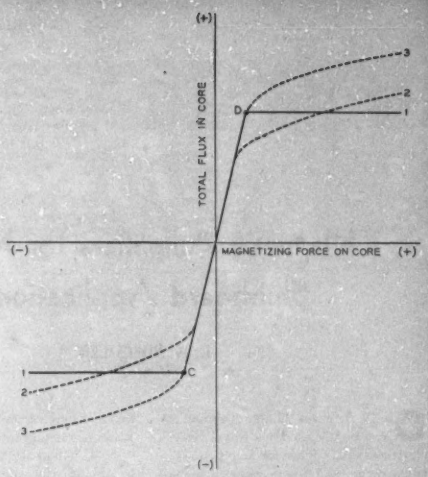


Figure 2. Hypothetical saturation curves (hysteresis effects neglected)

power gains of 1,000 to 1 to 5,000 to 1 the time of response probably will be in the order of two to five cycles. This, of course, is dependent on the magnetic material and the circuits used. For 60-cycle power the time of response will run from 1/10th to 1/30th of a second. If faster response is desired, 400-cycle power (which is becoming more available aboard naval vessels) can be used and the time of response cut to about 1/200th of a second. Special higher frequency power supplies can be used if much faster response is required, such as for an audio amplifier.

Problems in Magnetic Amplifiers

It is not intended to imply that magnetic amplifiers are new. They have been known and applied for many years, beginning in about 1915. Reference 1 lists some 213 articles and patents on magnetic amplifiers and allied subjects. Several companies over the past years have used this type of amplifier in limited quantities for certain naval applications, mainly voltage regulators. In such application, the magnetic amplifier usually was developed or designed around a particular piece of equipment, and no great effort was made to develop magnetic amplifiers for general use as a possible substitute in certain applications for vacuum tubes. Reports from Germany indicated that that country had taken a much broader interest in magnetic amplifiers. This may have been due to the limited number and variety of vacuum tubes available, but they did develop and use magnetic amplifiers for applications in which we would have used vacuum tubes. Developments have taken place over the last five years which are believed to make magnetic amplifiers more promising today than they were 15 or 20 years ago. Better magnetic steels have been developed and are being made available commercially. Better rectifiers of the germanium crystal and selenium types are now available and being further improved.

A brief discussion of the simple form of a magnetic amplifier will assist in understanding how these better steels and rectifiers can improve magnetic amplifier characteristics. Figure 1 illustrates a circuit which may be used. Assuming certain idealized conditions such as perfect rectifiers, a high impedance control winding, and an idealized saturation curve, the operation can be explained as follows. During one half cycle one reactor core, core *1* in Figure 1, is inactive; the rectifier is holding back (or absorbing) the voltage which may exist between *A* and *B*. The state of saturation in core *1* is determined by the current and turns in the control winding. During the following half cycle the rectifier associated with the winding for core *1* conducts and this winding will absorb voltage until core *1* saturates. If it saturates during this half cycle the portion of the voltage occurring after saturation appears across the load. The action on core *2* is similar but on the alternate half cycle. The voltage appearing across the load is alternating and of a shape somewhat similar to that which would be encountered with a grid-controlled thyratron tube in place of reactor core and rectifier.

Assuming the saturation curve has the shape shown by the solid line in Figure 2, the maximum voltage which can be absorbed across the power winding core *1* during its conducting half cycle would be caused by a flux change from the point *C* to the point *D* on the saturation curve. In order for the flux to start at point *C* there must be sufficient negative bias caused by the control current to bias the core during the nonconducting half cycle to the point *C* on the saturation curve. If a flux change of *C* to *D* is sufficient to equal the applied alternating voltage, practically no voltage will appear across the load when the cores are biased to the point *C;* and practically full voltage will appear across the load when the cores are biased to the point *D*. In actual practice, the saturation curves do not have such sharp corners; curve *3* in Figure 2 is a more typical saturation curve. The curve shown in Figure 3 represents the output curve obtained for one particular magnetic amplifier. It will be noted that approximately minus ten milliamperes is required to give negligible voltage across the load and plus ten milliamperes gives 34 volts across the 50-ohm load. The remainder of the applied 65 volts at plus ten milliamperes control current is lost primarily in resistance drop in the load winding, rectifier drop, and reactive drop in the cores due to the fact that the cores did not saturate sharply at plus ten milliamperes.

Magnetic Materials

Some features about the magnetic cores which can be noted from the foregoing are:

1. The steel should saturate at as low a magnetizing force as possible as this will require less power in the control winding. The maximum permeability alone of the

steel is not necessarily a good measure of the steel quality. Curves *1*, *2*, and *3* of Figure 2 might all have the same maximum permeability but give entirely different results when used in the magnetic amplifier. Permeability is usually an indication but not a measure of the power gain obtainable per stage.

2. The steel should have a very sharp knee on the saturation curve and the knee should occur as near maximum flux density as possible. Any additional flux after passing the knee of the curve causes induced voltages which makes it impractical to obtain anywhere near maximum voltage across the load. Very high values of magnetizing force are present on the core during the conducting half cycle. For example, with plus ten milliamperes, or 0.5 ampere-turn in the control winding of the magnetic amplifier illustrated in Figure 3, the maximum magnetizing force on the core during the conducting half cycle is about 700 ampere-turns. The saturation curve, therefore, should be flat from the knee to 700 ampere-turns.

3. The steel should have as high a flux density at saturation as possible. This permits smaller cores for the required voltage. In general the higher flux density steels, namely the silicon steels, do not have high permeability nor sharp saturation; therefore, the nickel alloy steels often are required notwithstanding their lower flux densities.

4. The steel should be consistent so that the two or more cores which make up the amplifier will be similar for best performance and so that various amplifiers made to the same specifications will have similar characteristics. Lack of consistency of magnetic materials has been the greatest problem to date in the magnetic amplifier program. A deviation of not greater than ±10 per cent in the magnetic characteristics would be considered very good. A deviation of ±20 per cent would be acceptable and much better than commercially available in the higher permeability materials.

5. The steel losses should be small. Eddy currents in the steel not only cause power loss but also cause the amplifier to have slower response. Hysteresis losses in addition to causing power losses may have the undesirable effect of causing a hysteresis effect on the input-output characteristic of the amplifier, that is, the output power may be different depending on whether the control is increasing or decreasing.

Developments in the field of magnetic materials which appear to have promising applications in magnetic amplifiers are as follows (Figure 4 illustrates magnetization curves for materials discussed):

1. The grain-oriented silicon steels. These have much higher permeabilities and sharper saturation than ordinary silicon steels. They should be particularly useful in power applications where the cost per pound of steel is an important factor.

2. The magnetically heat-treated silicon steels. These steels have similar features and advantages of the grain-oriented steels with the additional feature that more freedom in the choice of core structure is available since stamped laminations can be used.

3. The 50 per cent nickel-iron alloys which are now standard items with some steel companies. These have higher maximum permeabilities than the grain-oriented or magnetically heat-treated steels but are more expensive and have lower flux density at saturation than the silicon steels; therefore, their use will tend to be limited to applications in the low and intermediate power stages.

4. The 74- to 80-per cent nickel-iron alloys which are also commercially available. These are more expensive, have lower flux density at saturation, and have a higher maximum permeability than the 50 per cent nickel-iron alloys. Although their use may be limited to low power stages, they are useful for working at lower control signal inputs than any other commercially available steel.

5. The Supermalloy magnetic material developed by the Bell Telephone Laboratories but which is not yet commercially available. This material which has the highest permeability of any present material (about 1,000,000) should, when commercially available, permit working at signal levels even lower than can now be done.

6. The Permenorm-*5000Z* type of material. This material has a rectangular hysteresis loop (see Figure 5) and was originally developed by the Germans for mechanical rectifiers. It has the very desirable characteristic of saturating sharply at a low magnetizing force (about 0.15 oersted). The Naval Ordnance Laboratory has succeeded in developing a superior heat treatment process for this material and several companies are working on the problem of producing it commercially. Since it contains about 50 per cent nickel and is more expensive than other 50 per cent nickel alloys, its use probably will be restricted to low power and intermediate power stages.

Rectifiers

High quality rectifiers are essential in magnetic amplifiers. Some features desirable in the rectifiers are:

Low Forward Resistance. This is desirable in order that the power loss and the voltage drop will be kept to a minimum.

Very Low Back Leakage. A check on the magnetic amplifier in Figure 3 will illustrate the magnitude of this problem. During the conducting half cycle with a control current of ten milliamperes (0.5 ampere-turn), the

Figure 3. Output characteristics of a magnetic amplifier

Connections: See Figure 1
Winding: (each core)
Control—50 turns number 32 wire, 1.9 ohms
Output—700 turns number 30 wire, 14 ohms
Load: 50-ohm resistor
Power input: 65 volts, 400 cycles
Core: HyMu number 80, 0.006 inch
Ring laminations—3/4-inch inside diameter, 1¼-inch outside diameter, 5/8-inch stack

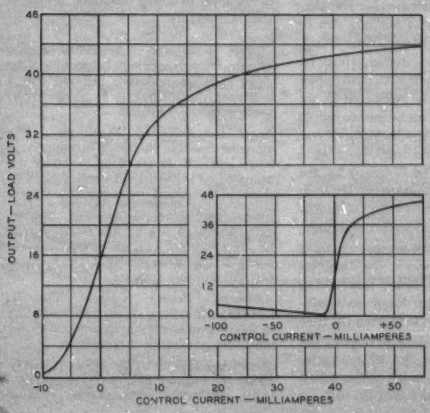
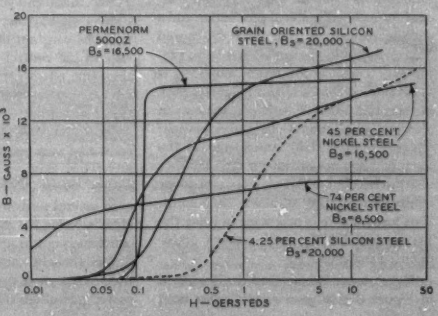


Figure 4. Normal magnetization curves for various steels which may be used in magnetic amplifiers

Data from tests by Naval Ordnance Laboratory



rectifier carries a maximum peak current of about 1.0 ampere. With minus ten milliamperes of control current a large portion of the alternating voltage of 65 volts rms appears across the rectifier. A back leakage in the power circuit of 0.1 per cent would amount to a peak of about 0.001 ampere or 0.7 ampere-turn, whereas, the control ampere-turns are supposed to be only +0.5 ampere-turn. The actual effect of leakage depends on the incremental hysteresis loops, operating point on the load curve, power circuit inductance, and so forth; and usually is less than indicated by this simple calculation. The leakage increases the amount of control current required and somewhat decreases the negative bias required resulting in an over-all loss in power gain.

Very Stable Characteristics. Any change in the rectifier's operating characteristic changes the performance of the magnetic amplifier in which it is used, especially if the rectifier has or develops leakage. Special consideration to the inverse voltage rating must be given in order to obtain the most stable operating characteristics. Consideration also must be given to the effect of moisture and temperature on rectifier characteristics. It is expected that the moisture problem will be avoided by using hermetically sealed rectifiers, probably enclosing them in the same container with the cores.

Circuits

Various electric circuit arrangements are possible with magnetic amplifiers but will not be discussed since circuits are beyond the scope of this article and circuits have received considerable discussion in the literature.[1-6] The particular circuit to be used will depend to a large extent on whether single-phase or 3-phase alternating power is available, whether a-c or pulsating d-c output is desired, whether the load is resistive or inductive, and the response time constant required. The magnetic circuit is very important and must be carefully designed if full advantage of the magnetic properties of the steel is to be realized.

For larger power applications, stacked laminations using air gaps may be necessary for most economical production and a sacrifice in gain or time constant caused by an air gap in the circuit has to be permitted. For most low power amplifiers it is considered that a closed magnetic path is essential. One solution is the use of stamped ring laminations and a toroidal winding. Thin strip laminations in the form of tape wound into a circular core also has been found satisfactory and about equivalent to the stamped ring cores. This form of core is necessary in using the Permenorm-*5000Z* and other grain-oriented material.

The stamped laminations as illustrated in Figure 6 give good results when used in magnetic amplifiers. For the rectangular lamination, a split winding-bobbin is placed on the center leg and all windings placed on this leg. This arrangement has advantages over the toroidal winding using the ring lamination since it permits winding a larger number of turns resulting in economical production of higher impedance windings; it permits obtaining a better copper space factor; and the winding does not stress the magnetic core. On other types of cores, special core boxes may be required to prevent stressing the core and destroying the good magnetic properties of the core material.

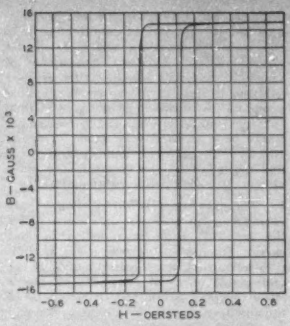


Figure 5. Hysteresis loops for Permenorm-5000Z steel

Data from tests by Naval Ordnance Laboratory

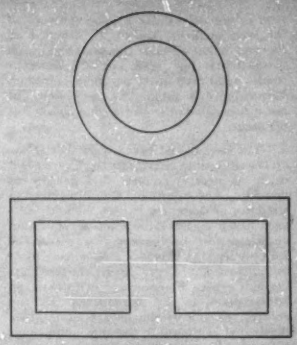


Figure 6. Core shapes used in magnetic amplifiers

Time Constant

The time constant of response for the majority of magnetic amplifiers is dependent to a great extent on the inductance of the control winding. The time constant of the control winding can be controlled best by using magnetic materials and magnetic circuits which require very small amount of control power. Fewer turns and more resistance in the control winding then can be used to reduce the time constant of the build-up of current in the control winding. Somewhat the same effect can be obtained by the use of negative feedback with the additional advantage of improving the linearity of the response. Both control winding resistance and negative feedback involve sacrificing gain to reduce the time response. Since gains of many thousand or even several million per stage are obtainable, reducing the gain to shorten the time constant is not as undesirable as might at first be thought. As indicated previously time constants of two to five cycles with a power gain of 1,000 to 1 to 5,000 to 1 per stage are considered to be reasonable values.

Applications

In general, magnetic amplifiers are believed to be usable for many applications where vacuum tubes presently are required and used. They are not believed to be usable in their present form at radio frequencies due to high eddy current losses in the core. At present 0.001- to 0.002-inch thickness is about the thinnest laminations which are obtainable commercially in high permeability magnet material.

Further development and use of ferrite materials may permit magnetic amplifier work at high frequencies.

Some naval shipboard applications for which magnetic amplifiers may be used are enumerated and described briefly in the following list:

Voltage Regulators. As indicated previously, there already have been applications along this line, and it is believed that a larger use of magnetic type amplifiers will be made in future regulators.

Speed and Frequency Regulators. The amplifier can take the speed (or frequency) signal and amplify the signal to control the prime mover to the desired speed. Developments of this nature are in process.

Line-to-Line Voltage Regulators. Magnetic amplifiers already are being applied for this purpose, and, as voltage requirements become more stringent, this application will probably grow.

Automatic Battery Charger Controls. Limited applications of magnetic amplifiers in this respect already have been made and others are now under consideration.

Servo Amplifiers. This use is probably one of the broadest fields for magnetic amplifier application. Many servo systems

aboard ship require amplifiers where the response time of a magnetic amplifier would be satisfactory. The Germans thought this application was very promising and actually used magnetic type of amplifier for a number of shipboard servo systems.[4]

Instrument Amplifiers. On several occasions the need has arisen for an amplifier to take very low level signals and amplify them to the point where they can be used to operate a meter of shockproof design.

Audio Amplifiers. By using an a-c power supply of 10,000 to 20,000 cycles, magnetic amplifiers appear to hold considerable promise of producing a high power, light weight, amplifier for this application. The main problem is in generating the high frequency power supply. Motor generators appear to be the quickest solution to this problem, but other methods not employing vacuum tubes are under consideration.

Control Relays. Magnetic amplifiers can find use in some of the metering or detecting circuits and in eliminating auxiliary control circuit relays.

Other Miscellaneous Applications. Devices such as temperature regulators, time delay elements, modulators, current regulators, and so forth, also provide applications for magnetic amplifiers, although at present little developmental work is underway for these particular applications.

Developmental Program

At present, considerable effort in the form of laboratory projects and contracts, is being expended to develop the theory and design principles of magnetic amplifiers, to develop magnetic materials which best will meet the needs of the amplifiers, to develop rectifiers of suitable characteristics, and to develop the amplifiers for certain applications in which their use appears advantageous. The progress of this work has been slow and most of the results of the investigations are not presently available; therefore much of this summary has had to deal with only the general results. Several applications not mentioned herein have been made in classified equipment and cannot be discussed.

It is hoped that eventually various types and sizes of magnetic amplifiers can be made available for general use so that they will not have to be designed for each application. The application then can be designed to use the type and size which most nearly meets the requirements of the job.

The work to date has confirmed earlier beliefs that magnetic amplifiers can be used for many shipboard applications wherein previously only other devices, such as relays or vacuum tubes, were considered practical. The ability to make magnetic amplifiers very rugged and service-free is considered to be of very great value to the Navy, especially for important circuits or systems in which reliability and continuity of service are of prime importance.

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4. Applications of Magnetic Amplifiers, W. E. Greene. *Electronics* (New York, N. Y.), volume 20, number 9, September 1947, pages 124–8.

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Discussion

H. F. Storm (General Electric Company, Schenectady, N. Y.): The following is intended to augment Buechler's excellent paper by providing some numerical data on the actual performance of an alternator equipped with magnetic amplifiers for voltage control and also for static excitation. The continuous rating of the synchronous 3-phase alternator is 278 kva, 220 volts: power factor 0.8 inductive; the 1-minute rating is 415 kva, power factor 0.5 inductive; frequency 420 cycles, 28 poles, open circuit field time constant 0.25 second; all a-c power for regulation and excitation is obtained from the alternator itself.

Significant for the performance of the set are the following items:

1. Speed of re-establishment of steady-state alternator voltage after a load change.
2. Steady-state alternator voltage as function of load.
3. Alternator current during short circuit.

The speed of response according to item 1 was tested by suddenly applying a 2 per unit load at a power factor of 0.4 inductive. Figure 1 of the discussion shows the alternator voltage and the field current during this disturbance. The alternator voltage very quickly re-establishes itself. Except for the first dip, which is unavoidable, the deviations from the steady-state voltage become too small for accurate numerical evaluation. In order to accentuate these deviations, an especially designed circuit was used which amplified these deviations by suppressing the zero line. The result is seen in Figure 2 of the discussion. Within a period of 310 milliseconds after application of the load, the alternator voltage is within ±1 per cent of

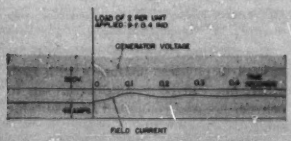

Figure 1. Transient response of 415-kva generator (3-phase 420-cycle) equipped with magnetic amplifier-type regulator, application of 2 per unit load

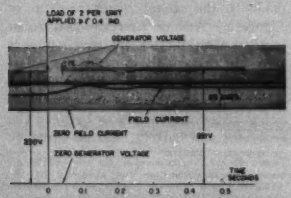

Figure 2. Transient response of 415-kva generator (3-phase 420-cycle) equipped with magnetic amplifier-type regulator, application of 2 per unit load

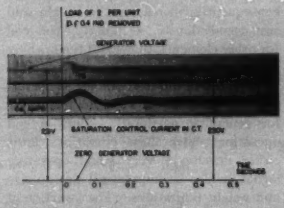

Figure 3. Transient response of 415-kv generator (3-phase 420-cycle) equipped with magnetic amplifier-type regulator, application of 2 per unit load

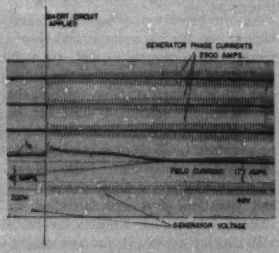

Figure 4. 415-kva generator (3-phase 420-cycle) under near short circuit conditions. Generator equipped with magnetic amplifier-type regulator

its finally sustained value. By suddenly removing the same load (Figure 3 of the discussion), the alternator voltage establishes itself in 130 milliseconds within ±1 per cent of the finally sustained voltage. The change of steady-state voltage between "on" and "off" is 0.45 per cent for the foregoing loading.

For rated loading, the change in steady-state generator voltage is 0.18 per cent between no load and rated, continuous load.

For reasons of selective circuit breaker tripping, it is sometimes desirable to obtain a sustained current of 2.5 to 4 per unit during short circuits. Figure 4 of the discussion shows an oscillogram of a short-circuit test in which a sustained short circuit of 4 per unit is obtained. At the point of measurement, the alternator voltage did not quite reach zero because of impedance in the cables to the short-circuiting breaker. This residual alternator voltage, however, is only coincidental and has no bearing on the performance of the system.

Proposed Circuit for Aircraft Reverse-Current Cutout

J. M. MARZOLF
ASSOCIATE AIEE

An investigation was undertaken to find a means of improving the performance of aircraft reverse-current cutouts under the extremely wide variations in operating conditions encountered in aircraft. The function of the reverse-current cutout is to connect its associated generator to the main bus when conditions are such that the generator is capable of delivering power to the bus and to disconnect the generator when the bus supplies power to the generator. Due to the wide variations in operating conditions encountered in aircraft, and the strict specifications imposed on its operation, it has been extremely difficult to obtain a completely satisfactory cutout.

Specifications for Ideal Aircraft Cutout

An ideal cutout designed for use on present aircraft should operate satisfactorily on any voltage between 20 and 30 volts, independently of the voltage regulator. It should close the main contactor to connect its associated generator to the bus when the generator voltage is 0.2 volt higher than the bus voltage, and should reopen it if a reverse current, equal in magnitude to five per cent of the generator full-load current, flows through the generator. Its operation must not be affected by wide variations in ambient temperature (−55 degrees centigrade to +70 degrees centigrade). It should not be affected by severe conditions of shock and vibration, dust and humidity, or altitude. It should compare favorably with existing cutouts in size and weight, and should be simple and dependable in operation. It should not provide a drain on the batteries when the generator is inoperative, and the pilot relay preferably should be universal in design so that the same pilot relay might be used on any generator, regardless of its current rating. Its power consumption should be limited to a reasonable value.

It was decided early in the investigation that the only satisfactory type of cutout was one which employed the differential voltage between generator and battery as the controlling factor. Thus, the differential voltage coil (or other controlling element) must operate on 0.2 volt in the forward direction, but must be able to stand at least 10 volts continuously in the reverse direction. (This is based on the assumption that a polarized auxiliary relay is used in the differential coil circuit which will not close until the generator voltage builds up to approximately 20 volts in the proper direction. If this is not done, the differential coil circuit must be designed to withstand 30 volts under normal conditions and 60 volts if the generator builds up in the reverse direction.) Even with this auxiliary relay, however, there is a ratio of full-load voltage to operating voltage for this device of 50 to 1 on the differential coil, making the use of a larger wire size necessary and, consequently, increasing the weight and size tremendously. Similarly, the coil (or other element), to control the opening of the cutout under reverse current, must have a ratio of full-load voltage to operating voltage of 20 to 1 (without allowing for generator overload). Thus, a coil operating under these conditions must either develop a very small operating signal or else be impractical due to size and weight limitations. The insertion of an ideal rectifier or current-limiter in the circuit would reduce these ratios to reasonable values. However, there is no known rectifier having a stable temperature characteristic which will pass appreciable current with 0.2 volt across its terminals and no suitable current-limiter has been found.

Construction of Proposed Cutout

In the course of the investigation many different ideas were investigated, the most promising of which is embodied in the circuit diagram of Figure 1. It consists, essentially of two units:

1. The main contactor with auxiliary contacts comprising a triple-pole double-throw switch.
2. The control element.

The control element consists of:

(*a*) A small balanced permanent magnet mounted on pivots and free to rotate between two contacts without restraining springs of any kind and actuated by three coils (differential, reverse, and residual), all wound on the same magnetic core between whose poles the permanent magnet is free to rotate. This element is schematically represented in Figure 2.

(*b*) A *2D21* thyratron tube and associated current regulator for its heater.

(*c*) A pilot relay having an operating coil, a holding coil, and a safety coil.

(*d*) A small insensitive polarized auxiliary relay in the differential coil circuit.

Components 1 and 2 do not need to be located in physical proximity with each other, but can be located according to convenience and connected by a suitable cable.

Method of Operation of Proposed Cutout

To explain its operation, reference is made to the circuit diagram given in Figure 1. Assume that the main contactor is open and the generator voltage is zero. In this condition, since the polarized auxiliary relay in the differential coil circuit is open, there is no connection either across the battery or between the generator and battery, and no stand-by

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J. M. Marzolf is with the Naval Research Laboratory, Washington, D. C.

power is consumed. The triple-pole double-throw switch is in the upper position as shown, and the permanent magnet control element is also in the upper position. When the generator voltage increases to approximately 20 volts in the proper direction, the polarized auxiliary relay in the differential coil circuit closes, thus making the cutout operative. This relay should be polarized to prevent burning out the differential coil if the generator voltage builds up in the wrong direction and could be accomplished very simply by use of a series rectifier in its operating coil circuit. Since the battery voltage, however, is still higher than the generator voltage, current will flow through the differential coil in the reverse direction, and hold the permanent magnet in the original position, and the cutout will not close. As the generator voltage rises, it reaches a value 0.2 volt higher than the battery voltage, at which time the flux from the differential coil is sufficient to overcome the residual flux in the magnetic core between whose poles the permanent magnet armature is mounted. This causes the permanent magnet to rotate and make instantaneous closure with the lower contact, impressing the full positive voltage on the grid of the thyratron tube through a suitable limiting resistor, and causing the tube to fire continuously regardless of subsequent grid voltage. This contact does not need to be continuous to cause the tube to fire; therefore, no contact springs are required on the permanent magnet armature and the mechanical force developed between the permanent magnet and the operating coil can be very small. The plate current of the thyratron tube passing through the operating coil of the pilot relay causes it to close. This causes the main contactor to close and at the same time the pilot relay is locked in by its holding coil. When the main contactor closes, it mechanically throws the auxiliary contacts which comprise the triple-pole double-throw switch to the lower position as shown and, in passing from one position to the other, instantaneously opens the plate circuit of the thyratron tube extinguishing it, but the pilot relay and main contactor remain closed. The permanent magnet is now in the lower position and is held there by the action of:

1. The reverse coil (as long as the normal load current flows in the main circuit from the generator to the battery).

2. The residual magnetism of the magnetic core of the control element, thus insuring that the thyratron tube will not fire again.

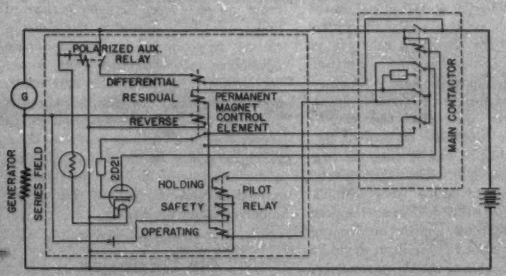
Assume that conditions in the main load circuit now change so that the generator voltage is less than the battery voltage, causing reverse-current to flow from the battery to the generator. This causes a voltage drop across the generator series field which is proportional in magnitude and direction to the load current. This voltage is impressed on the reverse coil and, at its operating point, is sufficient to overcome the residual magnetism of the magnetic core controlling the permanent magnet control element. This causes the magnet to rotate to the upper position and impresses a positive voltage on the grid of the thyratron again causing it to fire. This time, however, the current passes through the operating coil of the pilot relay in the opposite direction and overcomes the holding coil, allowing the pilot relay and main contactor to open. The current through the operating coil of the pilot relay is limited by a suitable resistor to preclude the possibility of the pilot relay being closed accidentally by current in this direction. In other words, the current through the operating coil is just sufficient to overcome the holding coil but not sufficient to close the pilot relay from the open position. As the main contactor opens, it mechanically returns the triple-pole double-throw switch to the upper position and, in so doing, interrupts the thyratron current long enough for the tube to be extinguished and the cycle can be repeated.

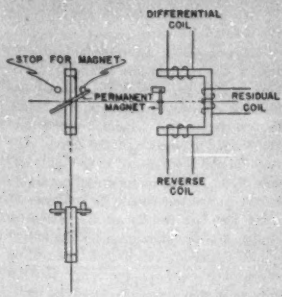
The purpose of the residual coil is to restore the residual flux in the magnetic core of the permanent magnet control element and thus insure uniform operation of the control element, regardless of the magnetic history of the core. The residual coil is connected in the plate circuit of the thyratron tube and its design is not critical but should be sufficient to carry the flux density of the magnetic core well above saturation for the lowest value of residual coil current that will be encountered (with the operating coil of the pilot relay, the residual coil and the limiting resistor for the reverse-current operation all included in the plate circuit of the thyratron tube and for minimum plate voltage). By insuring that the flux density in the core exceeds its saturation value on each operation of the cutout, the magnetic core will follow substantially the same hysteresis loop and the cutout will operate at the same point each time. In addition, a high value of residual flux is guaranteed to prevent spurious operation under conditions of shock or vibration at no load condition.

It will be necessary to include a ballast lamp in series with the heater of the thyratron tube in order to insure operation of the tube, without injury, under the wide variations of terminal voltage encountered. This ballast lamp will not affect the operation of the cutout but is inserted only as a protective device to prevent burning out the thyratron tube heater under the extremely wide variations in voltage that must be tolerated.

A third coil, designated as a "safety coil," should be wound on the pilot relay and connected in series with a suitable rectifier across the generator series field and designed to open the pilot relay under very large reverse-currents in the main circuit. This coil should be designed to operate the pilot relay at such a high value of reverse-current that, normally, it will not operate the cutout. It is merely included as a safety feature to insure that the cutout will open even though the thyratron tube were to burn out while the main contactor was closed. It would insure that the cutout "fail-safe." The coil should be designed with a suitably large time constant so that the pilot relay will have sufficient time to open, regardless of the steepness of the reverse-current surge characteristic, before the safety coil current reaches a value sufficiently high to hold the relay closed by current in the reverse direction. Thus, the pilot relay should open, mechanically, very rapidly and the current in the safety coil should

Figure 1. Circuit diagram for proposed reverse-current cutout





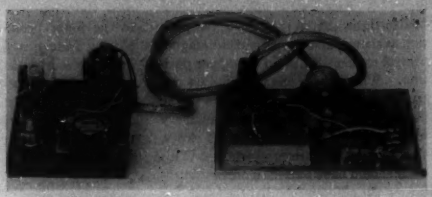


Figure 2 (left) Schematic representation of magnetic circuit of control element for proposed reverse-current cutout
Figure 3. Experimental model of proposed reverse-current cutout

rise relatively slowly regardless of the surge voltage applied across its terminals. If these two factors are properly taken into consideration in the design of the pilot relay, the pilot relay will never remain "locked" closed by the safety coil current regardless of the steepness of the reverse-current surge.

Factors to Be Considered in Design

In the design of the cutout, certain points should be kept in mind. The control element should be hermetically sealed to prevent interference by dust and moisture and prevent "adjustment" by poorly trained technicians. If sealed in a dry atmosphere, it would prevent interference by frost or ice on the operation of the control element. If the cover were made of iron or steel, it would serve also as a magnetic shield to prevent interference by any stray fields that might be present. The permanent magnet should be balanced and mounted so that vibration, shock, position, or physical changes in dimensions due to extremes of temperature would not affect its operation. It should also be as small and light as practical, in order to be quick acting, and its bearings should be as frictionless as possible. Should future investigations indicate that this cutout is too responsive to transients on the system, the speed of response can be decreased easily by placing a short-circuited copper slug around the magnetic core of the control element. The core material for the control element, should have a relatively high residual magnetism to insure stability under generator no-load conditions by developing sufficient force to hold the permanent magnet stationary under all conditions of shock and vibration, and should also have a low coercive force so that a small applied signal, of the proper polarity, will operate the cutout. It is suggested that possibly use of some of the newer magnetic materials having square hysteresis loops might be advantageous. The permanent magnet in the control element should be constructed of a material having a high coercive force and should be made as light and small as possible.

Advantages of Proposed Cutout

A cutout based on this principle would be practically independent of temperature, altitude, and terminal voltage (from 20 to 30 volts) and, since it operates on a very small number of ampere-turns, the differential and reverse-current coils can be designed for a 5,000 and 2,000 per cent overload respectively, and still be relatively small in size, thus overcoming the problem outlined before. Also, since the voltage drop across the series fields of all aircraft 28-volt d-c generators is approximately the same at full load, regardless of power rating, for the equalizer connection to the voltage regulator, the same cutout could be used on any system regardless of generator rating. Furthermore, the components may be located remotely for convenience, accessibility, and so forth, it merely being necessary to connect the contactor and control element by means of a suitable cable. It would lend itself also to an installation wherein the control element is made in a hermetically-sealed plug-in type unit which could be removed and a new one installed quickly in case of failure. The cutout can be made small and light since it operates on a pulse technique and thus, the operating coil on the pilot relay, the residual coil on the control element and the thyratron tube can be overloaded instantaneously. Furthermore, the holding coil on the pilot relay, although designed for continuous operation, operates only when the relay is closed under conditions of zero air gap and would thus be smaller than the conventional relay coil. The power loss in the cutout would be low since no additional shunts or other devices are required in the power circuit, and the only appreciable power input to the cutout would be that to the heater of the thyratron tube. Since there are no springs or mechanical restraining torques on the control element and no contact springs (instantaneous contact only needed), it can be made quick-acting and the permanent magnet will never be subjected to surges in the reverse direction that might reverse its polarity accidentally. Provided the surge did not build up faster than the permanent magnet could move, the magnet would never be subjected to a reverse polarity, since it always lines up mechanically with the existing field. If the control element magnet is made sufficiently light and friction-free, this reverse polarity would not be a problem since, the faster the surge increased, the greater the mechanical force and the quicker the element would move.

Experimental Results

An experimental model was constructed and tested to demonstrate the soundness of the principles involved. A general view of this model is given in Figure 3 which shows both components connected by a suitable cable. There was no attempt made in this model to keep the size and weight factors to a minimum. It performed entirely satisfactorily and as predicted under the variations of voltage for which it was designed. However, since no attempt was made in the mechanical design to take care of the factors of vibration, shock, hermetic sealing, variations in position, changes in physical dimensions due to temperature variations, and so forth, no tests were actually performed under operating conditions. These should suitably be postponed until the necessary developmental engineering could be accomplished. Sufficient trials were made, however, to convince the author that the principles involved are sound and that a properly designed cutout based on these principles would be entirely satisfactory under the extremely wide variations of conditions encountered by this device under operating conditions.

No Discussion

A Polarized Relay as an Aircraft Control Element

ROYCE E. JOHNSON
FELLOW AIEE

FRANK A. GLASSOW
ASSOCIATE AIEE

DEMAND for simple, lightweight, reliable control equipment for automatic controls in aircraft has resulted in the development of a sensitive polarized relay having such a variety of applications that its characteristics are of general interest. In this relay a permanent magnetic field is combined with that of the relay coil in a manner to produce great sensitivity without the loss of stability and inherent strength. These characteristics have been found most useful in mobile equipment where direct current is the primary source of electric power and where control devices must be held to a minimum in size and weight.

To be suitable for use in aircraft controls, a polarized relay must embody the extraordinary features of design dictated by the severe environmental conditions it will encounter. Not enough emphasis can be placed on the necessity for rugged design, simplicity, and ease in servicing control devices. In determining the qualifications of a polarized relay as an aircraft control element, it is analyzed in regard to the following characteristics:

1. *Sensitivity.* Sensitivity should be adequate for the control function, but it should not be stressed at the expense of stability.

2. *Speed of Response.* Operating times for contact closure and release should be low. Under three milliseconds is desirable for high-speed control.

3. *A-C Sensitivity.* Response of the relay to various frequencies is an important factor in some applications.

4. *Vibration.* The effect of vibration warrants detailed study and it may require countermeasures to minimize adverse performance.

5. *Over-Voltage.* Although only a few millivolts may operate the polarized relay, the effect of excessive voltage applied to the coil should be considered.

6. *Stray Field Sensitivity.* Careful consideration should be given to stray fields which may affect a sensitive polarized relay.

7. *Arc Suppression.* To insure long-troublefree operation, arc suppression is usually recommended. Correct suppressor values are dependent on load and type of contact material.

8. *Contacts.* The most desirable contact material ordinarily depends on load and ambient conditions. Extensive experimentation yields the most accurate criteria.

This analysis of characteristics is applied to the polarized relay described in this paper, and technical data appear in the form of graphs and oscillograms. Current uses of this relay may be classified as

1. Remote Positioning—cowl flaps, landing flaps, trim tabs, valves, dampers, nose wheels, cameras, gun sights.

2. Synchronization—landing flaps, trim tabs, flap-stabilizer.

3. Temperature Control—gasoline or electric heater cycling, cockpits and cabins, carburetor air, engines, wing and empennage surfaces, windshields, camera compartments.

Typical examples of the basic applications are described in detail in order to illustrate the basic principles involved. The examples serve also to point out the relative simplicity of control circuits using polarized relays and the few precautions that promote reliability.

Description of Relay

The basic principle of the relay is essentially that of a magnetic bridge similar to the one shown schematically in Figure 1. Symmetrical pole pieces cause the magnetic flux to pass through two identical air gaps while an armature extends between the midpoints of these two air gaps, dividing each gap into two sections or arms of the magnetic bridge. The armature in this magnetic circuit corresponds to the galvanometer in a Wheatstone bridge circuit, in that any magnetic potential difference between the centers of the magnetic gaps causes a flux to pass through the armature. It follows that if there is flux through the armature the flux densities in adjacent magnetic arms are different and therefore there will be a net transverse force at each end of the armature.

Flux from the relay coil, as is evident in the figure, must pass through the armature. In so doing, it increases the flux in one pair of diagonally-opposite magnetic bridge arms and decreases the flux in the other pair. The resultant unbalance in magnetic forces rotates the armature against the torsional spring restoring force.

Mathematically the magnetically produced torque on the armature, while it is in the central position, may be stated qualitatively as

$$T \propto \Phi_M \Phi_C$$

where T is the torque,

Φ_M is the main flux from one pole tip to its mating tip

Φ_C is the flux set up by the relay operating current

After the armature deflects slightly this torque is augmented by an additional torque produced by a slight redistribution of, and increase in, the main

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Royce E. Johnson and Frank A. Glassow are both with the Barber-Colman Company, Rockford, Ill.

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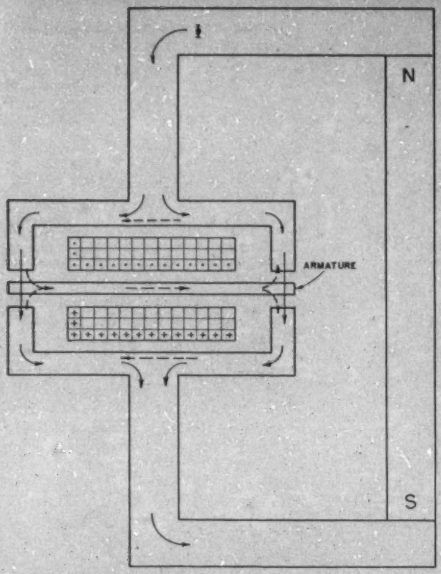

Figure 1. Basic magnetic bridge

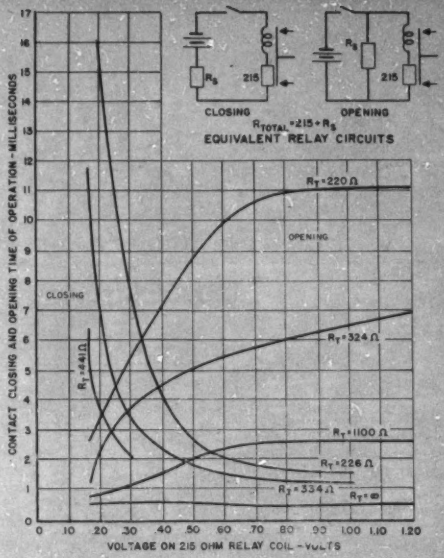

Figure 3. Representative operating and release time curves (215-ohm 2,950-turn coil)

flux, part of which is diverted through the armature by the shortening of the gaps to poles of opposite polarity at either end of the armature.

Bisymmetrical construction of the relay, which is made possible by the magnetic bridge principle, facilitates accuracy in production and provides inherently good balance for the armature. Two parallel Alnico magnets, one at either end of the relay as shown in Figure 2, energize the pair of low retentivity pole pieces which serve as yokes to hold not only the magnets but also the armature subassembly and the stationary contacts.

The armature subassembly shown in Figure 2 is held by clamping the ends of the side rails between the pole pieces. These side rails are an integral part of the beryllium copper punching which contains also the torsional arms and contact fingers. This punching, after being formed and heat treated, has a Nicaloi armature riveted to it.

Dual purpose magnetic shunts provide means for accurately adjusting the sensitivity and, within a limited range, for centering the armature with respect to the poles. Four symmetrically located shunts have been found desirable.

Stationary contacts of this single-pole double-throw relay are carried on brackets mounted on the lower pole piece. Insulated studs permit the stationary contact screws to be adjusted accurately.

Three-point mounting of the relay structure on the phenolic base eliminates the decalibrating effect of mechanical strains set up in the base when it is stressed. Phenolic bases for mounting directly on a panel or with integral vacuum-tube type octal bases are standard.

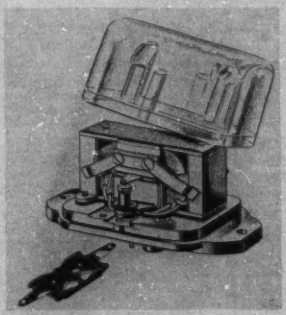

Figure 2. Relay and armature subassembly (Patent number 2,443,784 and patents pending)

Characteristics

Sensitivity. An input of 40 microwatts (1.3 ampere turns) as the operating or sensitivity power of the single coil polarized relay has been the minimum for production quantities. However, an excitation of 1.9 ampere turns or more provides better stability and facilitates calibration. Multiple coil relays have the same ampere turn requirements.

The release voltage is less than the operate voltage by an amount which varies somewhat with the sensitivity of the relay, the retentivity of the high permeability armature and pole pieces, and the amount of voltage applied in excess of the minimum operating voltage. For voltages up to 10 times sensitivity voltage the release voltage ranges from about 5 to 65 per cent below the operating voltage.

Operating Speed. The relatively low mass of the armature and the stiffness of the torsional spring permit rapid response of the relay. Data for closing and opening times for several voltage and series resistance combinations with a typical relay are shown in Figure 3.

Typical oscillograms showing the interval between the application of a square wave voltage and the closing of the con-

tact are shown in Figure 4. These oscillograms were taken on a cathode-ray oscilloscope using a 1,000-cycle timing wave on one pair of plates and a voltage across the capacitor in an R-C circuit on the other pair of plates. The relay and R-C circuit were energized simultaneously spreading the 1,000-cycle timing wave across the fluorescent screen. An auxiliary voltage was superimposed on the 1,000-cycle timing signal by the operation of the polarized relay contact, making it possible to determine the time delay by counting the number of cycles before the contact closed.

One or two short contact bounces of less than 1 millisecond duration were observed for operating voltages between 1.5 and 4 times sensitivity voltage. The duration of these bounces is so much shorter than the operate and release times of the d-c relays ordinarily used as secondary relays[1] that chatter of the secondary relays would not be expected from this bouncing. To the knowledge of the authors, no difficulty has resulted from this bouncing. The effect of this bounce can be largely eliminated, if necessary, by the use of an arc suppressor having adequate capacitance. However, actual usage conditions are generally different than for these oscillograms in that the voltage on the relay coil changes gradually in most applications rather than by large increments. Gradual application of voltage to the coil does not cause contact bounce.

Frequency Response. Figure 5 indicates the range of frequency to which the relay will respond. Conditions approaching resonance exist in the vicinity of 90 and 190 cycles per second. The smaller irregularities in the curves appear to be related to harmonic vibrations in the armature subassembly.

Equivalent Inductance. Calculations of equivalent inductance, where the effects of armature inertia, spring constant, and magnetic reaction on the coil are considered as part of the coil inductance, yield values between 1 and 2.8 henries for "make" operations of the relay for the conditions of Figure 3. The equivalent inductance is for a circuit in which the current reaches the magnitude of the sensitivity current of the relay in the same time interval as measured for the contact to close.

Under contact opening conditions the equivalent inductance varies over a much wider range, being between 9 and 6 henries for excitations ranging from 105 to 125 per cent of sensitivity, and decreasing to about 1 henry for 600 per cent of sensitivity excitation. This greater variation is to be expected for opening operation because excitation with current in excess of sensitivity current causes energy to be mechanically stored in the contact finger which yields slightly as a spring when torque is exerted by the armature. Conversion of the potential energy to kinetic energy in the armature upon de-energization of the relay affects its speed and also the armature reaction voltage induced in the coil.

Inductance calculations based on alternating current and voltage measurements with barely enough input to close the contacts indicate an inductance of about 4.5 henries between 20 and 70 cycles, decreasing to practically zero at about 90 cycles and again at 190 cycles. When excited sufficiently to vibrate the armature slightly but not close the contacts, the impedance of the relay is inductive below the lower resonant frequency and above the higher resonant frequency. Between the two resonant frequencies the reactance is capacitive. The effects produced by limiting the armature travel, as the contacts do, cause considerable variation in the impedance as the input is increased above the amount which barely closes the contacts.

Vibration Sensitivity. Although the armature construction is such that it is very well balanced, it will be affected by rotational acceleration in a plane perpendicular to its torsional axis. When mounted so that linear acceleration is parallel to the long dimension of the armature the effects of vibration are somewhat more noticeable than for other directions of acceleration. Vibration parallel to the torsional supports is least noticeable.

The use of secondary relays with slugs or a short-circuited coil, or a resistor in parallel with the secondary relay coil, aids in eliminating chatter of the secondary relay contacts when the polarized relay is subjected to vibration. A resistance-capacitance arc suppressor on the polarized relay contacts is also of appreciable value in overcoming the chatter producing effects of vibration. Curves showing the range of polarized relay voltages which will permit chattering of the secondary relay when both relays are subjected to a range of vibration frequencies are shown in Figure 6.

In many cases the most satisfactory method of preventing contact chatter either in the polarized relay or the secondary relay is that of electromagnetic detenting. Two auxiliary detenting coils are wound over the main coil of the polarized relay, one in series with each contact,

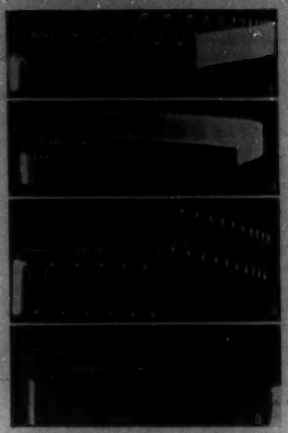

Figure 4. Oscillograms of contact operation (215-ohm 2,950-turn relay—1,000 cycle timing wave)

Operation	Coil Voltage	Circuit Resistance, Ohms
(A) Make	0.60	226
(B) Make	0.16	334
(C) Break	1.37	220
(D) Break	0.34	1,100

so that when either contact closes the load current sharply augments the initial excitation to develop a definite pressure on the contact. The detenting magnetomotive force generally is between 10 and 25 per cent of the sensitivity magnetomotive force. Consequently, when detenting is used the polarized relay contacts open at 10 to 25 per cent less voltage than would otherwise be the case. Detenting also permits the contacts to open more rapidly at "break."

Over-Voltage. Considerable excess voltage can be applied to the relay coil without damaging the relay. For example, a 215-ohm relay having a sensitivity of 0.15 volt (0.001 watt), had its control point shifted slightly by retentivity of the pole pieces when subjected to 25 volts. The shift was removed by passing a reverse current through the coil as occurs in normal operation. A one second application of 220 volts of direct current to an 88-ohm 1,900-turn coil produced a temperature rise of 55 degrees centigrade and permanently affected the calibration. Application of 220 volts to a 215-ohm 2,950-turn coil affected the calibration but not

to such an extent that demagnetization of the pole pieces and the readjustment of the magnetic shunts could not restore its calibration.

Continuous input of $2^1/_2$ watts (23 volts on a 215-ohm coil), uniformly divided among the operating coils if more than one exists, will not cause excessive heating or damage to the relay.

Magnetic Field Sensitivity. Stray fields have some effect upon the operation of the unshielded polarized relay. Thirty-five ampere turn excitation in a 4-inch diameter coil outside the relay but concentric with its coil closed a contact on a relay adjusted for 1.5 ampere turn sensitivity. Between 70 and 90 amperes in a straight conductor across the top of the relay cover or under the base of the relay, in a plane perpendicular to the relay coil axis, also closed the contact.

Stray field from adjacent conductors or secondary relays controlled by the polarized relay may frequently be turned to good use by positioning the polarized relay with respect to the source of stray field so that a satisfactory degree of detenting will exist.

Arc Suppression. For a 28-volt series motor load (1.5 ampere stall current), a suppressor consisting of a 33-ohm resistor in series with a 5-microfarad capacitor was effective when connected across the contacts. For relay loads a higher value of resistance is necessary for best results and the capacitance should be less. Upon opening of the contacts the inductive impulse of a secondary relay charges the arc suppressor capacitor. Should the polarized relay contacts close shortly after opening, as a result of vibration, for example, while the arc suppressor capacitor is still charged to a high voltage, the discharge current will be excessive if not limited by adequate resistance, causing welding of the contacts. Obviously when the capacitor is small it will be charged to a higher voltage than a large capacitor, and consequently, the peak of the transient discharge current will be greater if discharge occurs at peak voltage.

Resistors in parallel with the coils of secondary relays have, in some cases, been found more effective in avoiding contact welding than conventional R-C suppressors. A typical value of resistance is 470 ohms in parallel with a 750-ohm 10-to-13 henry relay. However, it has been observed that with only the parallel resistor there is tendency for pin point buildup on the negative contacts. This negative contact pin point apparently results from the cold emission which takes place when the contact gap is less than 0.0003 inch.[3]

A combination of a resistor in parallel with the coil of the secondary relay and an R-C suppressor across the contacts of the polarized relay has five desirable features.

1. The shunt resistor absorbs part of the coil energy when the contacts are opened, thereby reducing arcing.
2. The rate of opening for the secondary relay is appreciably decreased, minimizing effects of vibration.
3. Physical size of the capacitor is reduced.
4. Capacity and the resultant energy discharged through the contacts at make are less than when no shunt resistor is used.
5. The constants can be adjusted to prevent the pin point formation on the negative contact.

A half-wave selenium disk rectifier in parallel with the coil of the secondary relay to absorb the transient when the polarized relay contacts open has been found unsatisfactory for preventing contact welding. Upon first thought it appears that the rectifier would make an ideal arc suppressor, but a study of Glen Ramsey's paper,[3] indicates that there can be a reverse current through a rectifier when it is first energized. This apparently explains the failure of the rectifier to satisfactorily prevent contact sticking.

Contacts. Because of the low contact

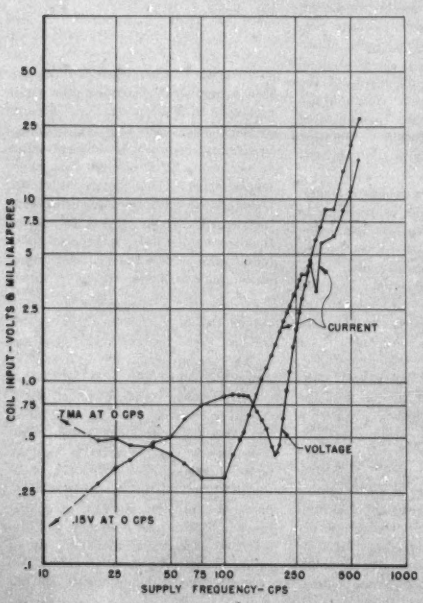

Figure 5 (left). Frequency response characteristics

Input barely sufficient to cyclicly close contacts. (215-ohm 2,950-turn coil, 2.05 ampere-turn sensitivity)

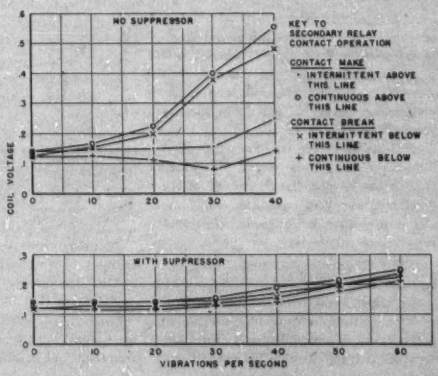

Figure 6. Effect of vibration on operating voltage

Contacts referred to are those of the secondary relay which was controlled by the polarized relay. Both relays were vibrated with 0.06 inch total amplitude, perpendicular to plane of armature

Table I. Typical Contact-Life Data

Nature	Lead Volts, Direct-Current	Lead Amperes	Cycles of Operation	Coil Input, Sine Wave Cycles per Minute	Coil Input, Sine Wave Ratio of Peak to Sensitivity of Volts
Resistance	30	0.5	400,000	6	1.1
Relay	30	0.04	over 3,000,000	6	1.3
Relay	120	0.04	over 10,000,000	60	10.0*

* Relay tested by a user with square-wave input and germanium rectifier suppressor.

pressure and slow armature movement, particularly when the polarized relay is used as a contact-making galvanometer in resistance bridge circuits, the contact duty differs from that in conventional relays. Fine silver contacts were originally used for controlling small reversible 24-volt series motor actuators without the use of auxiliary relays and without detenting coils on the polarized relay. Motors with fixed load current rating of 1 ampere and stall current of 1.5 amperes, 28 volts, have been successfully controlled. However, under plugging conditions the contacts occasionally stuck and, if the polarized relay was used too long without proper servicing, contact material transfer would eventually cause difficulty. The trend now is to use electromagnetic detenting in conjunction with interlocked secondary relays to handle the reversing motor current.

When sulphurous atmospheric conditions exist and only small currents, less than 0.25 ampere, are to be controlled, platinum-ruthenium alloy contacts are superior to silver. The platinum-ruthenium contacts are more stable chemically than silver but will stick due to welding on lower current than will silver contacts.

Laboratory tests, using suitable arc suppression with no electromagnetic detenting, indicate that contact life is reasonably long if the contacts are not overloaded. Typical life data for silver contacts are shown in Table I.

Testing technique for the contact performance of the polarized relay is rather involved. It must simulate the operating conditions not only as to contact load but also in magnitude and rate of change of coil current. Sudden application and removal of the operating current will produce much more rapid "make" and "break" than occurs when the resistance of the temperature sensitive element of a bridge responds to temperature changes. Minute sticking can be detected only with slowly varying operating voltage. The effect which rate of voltage change has on contact life is partly indicated in Table I.

Operation counters are not satisfactory for detecting minor degrees of malfunctioning because it is necessary to record deviations from uniform "make" and "break" voltages. Present test procedures involve two different methods. One uses a motor driven potential dividing rheostat to supply the coils with slowly alternating filtered voltage of peak value slightly greater than sensitivity voltage and continuously recording the coil voltage in such a way as to indicate the voltage at which each contact "makes" and "breaks." The other method is to use an electrothermal self-cycling circuit under controlled ambient conditions, recording on a multipen operation recorder the length of the contact open and closed intervals.

Field of Application

A polarized relay with near-galvanometer sensitivity naturally suggests itself as the detecting element for control systems, particularly those using resistance bridge and other differential circuits for their sensing elements.

The relay with a single coil and a floating armature which can close either of two contacts, depending upon the direction of current through the coil, is suitable for many control systems based on bridge circuits. The addition to the relay of detenting or biasing coils often improves the performance of a polarized relay control system. Secondary or slave relays increase the power handling capacity of the polarized relay and can provide interlocking, control point shift and other features desirable in certain control systems.

Other control applications, usually not involving a bridge circuit, utilize double coil differential polarized relays. With such a relay the contacts will remain open as long as the difference between opposing magnetomotive forces is less than the operating magnetomotive force. Where the currents which are compared in the differential relay coils are large relative to the net operating current, the current carrying capacity of the coils becomes a factor. Likely applications for the 2-coil differential relay are in the plate circuits of electronic amplifiers, for directly comparing the currents through resistors, or for detecting the difference between two voltages, one of which may be constant.

Certain applications, using either the single or double coil relay, use only one pair of contacts or use both pairs of contacts with the relay adjusted for snap action with no intermediate floating position. Examples are, respectively, on-off temperature control and selective channel or circuit control.

Since most modern aircraft have 28-volt d-c electric systems and even those now using 400-cycle alternating current ordinarily include some d-c power, the d-c polarized relay finds its greatest use as an aircraft control element. The small size and weight of the device and the relative simplicity of control systems centered around it further enhance its field of application.

Typical Control Systems

As examples of the control systems which incorporate a polarized relay as the detector, several typical commercial and military aircraft systems will be described.

Positioning. Perhaps the first application of the polarized relay described in this article was the straightforward positioning of an actuator from some remote point. Actually it was the need to accomplish this control function in aircraft which led to the development of the polarized relay we are considering. A circuit illustrating remote positioning is shown in Figure 7. A bridge circuit is constructed of two potentiometers, one, the transmitting potentiometer which is manually operated, and the other the follow-up potentiometer, which is directly coupled to the output shaft of the actuator. The main coil of the polarized relay is connected between the wiper arms of the potentiometers so that when the transmitting potentiometer is moved counter-clockwise, current flows from C_1 to C_2 in the polarized relay. This causes the armature to close W to R resulting in a counterclockwise movement of the actuator. This movement continues until the follow-up potentiometer rebalances the bridge circuit causing the armature to resume a floating position which stops the actuator.

Positioning accuracies of ± 1 degree and long-trouble-free operation are possible with controls of this type provided a few simple precautions are followed. Several of these measures as applied to the circuit shown in Figure 7 are enumerated below:

1. The sensitivity and differential of the polarized relay, speed, friction, and inertia

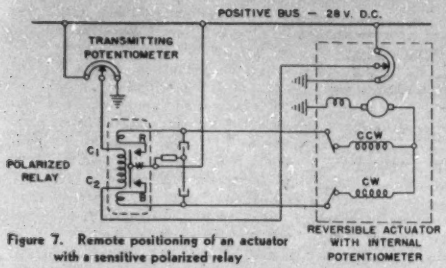

Figure 7. Remote positioning of an actuator with a sensitive polarized relay

of the load, and characteristics of the actuator should be co-ordinated to prevent hunting.

2. The potentiometers should have good resolution and linearity to insure accuracy of positioning. Wiper arms should be equipped with Paliney number 7 or similar alloy contacts for long life and good conductance of small currents.

3. Since the relay contact load is inductive, arc suppression is necessary to protect the contacts.

4. Small detent coils consisting of one turn each are built into the polarized relay in order to eliminate contact chatter. They improve also positioning accuracy since the proper amount of detenting will cause the actuator to stop at the center of the dead band of the relay.

5. Power for the actuator is isolated from power for the bridge circuit so that I-R line drop caused by the actuator will not reduce the bridge voltage, causing an oscillatory chatter of the relay.[4]

The physical components of a remote positioning circuit are shown in Figure 8. In this figure, the transmitting rheostat and the polarized relay are housed together in an explosion-resistant case. The control station is shown with a reversible rotary actuator having a torque of 50 pounds inches, operating through 180 degrees at 1 rpm. The combined weight of the control station and the actuator is 2.8 pounds.

The use of the polarized relay for remote positioning is not confined to small actuators such as that shown in Figure 8, for the relay may also operate secondary power relays designed for carrying heavy motor loads. Successful applications in aircraft include remote positioning of cowl flaps, landing flaps, trim tabs, valves, dampers, and nose wheels. These represent only some of the more common positioning applications. Many others are in various stages of development and will be available soon to answer the increasing demand for simplified controls in aircraft.

Temperature Control. One of the best known and most widely used applications of the polarized relay is that of temperature control in aircraft. These applications have included control of temperatures in cockpits, cabins, manifolds, engines, wing and empennage surfaces, and windshields. The general method of accomplishing temperature control again involves the resistance bridge, one or more arms of which consist of temperature sensitive resistors. Both positive and negative coefficient resistors are used and they are constructed in various shapes to permit sensing temperature in the desired media in the most effective manner. Figure 9 illustrates several typical temperature-sensing elements, each designed for a specific application.[5]

In some of these control systems, a simple "on-off" type of control is sufficient. This is represented by the rapid cycling of a gasoline heater used to provide heated air to a cabin or wing duct or by the control of electric power dissipated in an electrically-heated windshield.[6]

The basic circuit of a control system used for cycling a gasoline-burning heater to maintain a prescribed cabin or cockpit temperature is shown in Figure 10. Each arm of the bridge circuit and the polarized relay coil has substantially the same resistance when balanced at normal temperature. A change in resistance in any arm which unbalances the bridge in the direction calling for heat

Figure 8. Remote positioning control assemblies

Polarized relay and transmitting rheostat are combined in an explosion resistant housing

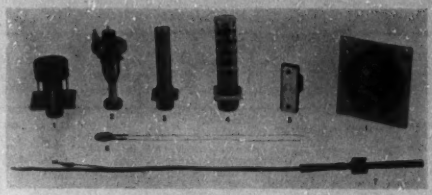

Figure 9. Resistance sensing elements used in temperature controls with a polarized relay

(1) Cabin exhaust duct thermistor elements
(2) Carburetor air thermistor element
(3) Compensating wire element
(4) Supply duct wire element
(5) Wing thermistor element
(6) Windshield thermistor element
(7) Cylinder head thermistor element

causes the polarized relay to energize the heater cycling relay which controls the heater fuel solenoid. The cabin elements are made of thermistor disks having a large negative coefficient, and being much more responsive than the heater elements, composed of fine wire with a positive coefficient, a small change in temperature at the cabin elements will cause a large change in the control point at the heater discharge. Many applications are in service where lowering the cabin temperature 1 degree Fahrenheit will increase the heater discharge temperature approximately 30 degrees. Other ratios can be provided readily.

Adjustment of the cycling rate from approximately 2 to 10 cycles per minute is accomplished with the frequency rheostat and a second set of contacts on the cycling relay. This method biases the bridge circuit to shift the control point a suitable amount. Shift in control point attributable to the drooping characteristic of the control is effectively offset by the reset element which responds to changes in ambient temperature.[7,8]

The polarized relay in a temperature-responsive bridge might be used also to operate a reversing motor which in turn operates a mixing valve. Proportioning control is accomplished by coupling a follow-up rheostat to the reversible actuator which rebalances the bridge after a given movement of the actuator. Such a control system is represented by the assemblies shown in Figure 11, which comprise a wing anti-icing control system. The circuit simply consists of a typical bridge with one thermistor sensing element suitable for riveting to the inner surface of the wing. The polarized relay operates two small secondary relays which position the proportioning actuator to

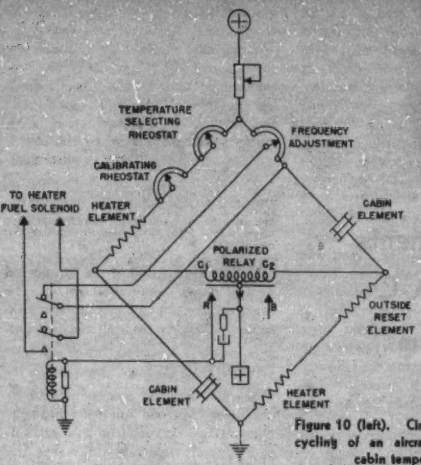

Figure 10 (left). Circuit elements for rapid cycling of an aircraft gasoline heater for cabin temperature control

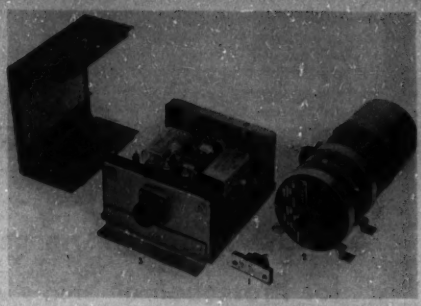

Figure 11. Components of wing anti-icing automatic control system

(1) Thermistor sensing element
(2) Mixing valve actuator
(3) Control cabinet (cover removed) containing polarized relay, motor relays, and fixed circuit components

satisfy the heat requirement of the wing.

The use of the polarized relay as a control element in aircraft temperature control has resulted in many simple, reliable systems which usually offer a considerable saving in weight and cost. The wide variety of resistance-type temperature-sensing elements, both positive coefficient and negative coefficient, high or low time constant, flat and thin or long and narrow, have made possible the adaptation of this control method to practically any temperature regulating problem existing in aircraft.[8] It is felt that even greater possibilities of new application exist with proper design of control systems using the polarized relay.

Advantages and Limitations

As with any unusual electrical or mechanical device, the possibility of misapplication is ever present. Almost as frequently as the many advantages of the relay described are not fully realized, the several limitations are overlooked. In order to evaluate the capabilities of the polarized relay discussed in this article, the features of the relay and its application are enumerated below:

Advantages

1. The polarized relay has extreme sensitivity, operating on as little as 40 microwatts or 1.3 ampere-turns.

2. The relay weighs only 6.5 ounces, and its size is $2^5/_{16}$ by $1^7/_8$ by $1^7/_{16}$ inches.

3. The relay is structurally strong and has withstood, without mechanical failure, severe operating shocks encountered in aircraft and missiles. No bearings are used in the armature, the only moving part of the device.

4. Coil resistances may vary from a fraction of 1 ohm to 10,000 ohms in order to match associated electric circuits.

5. Rugged contacts of either fine silver or platinum-ruthenium alloy will carry up to 1/2 ampere at 28 volts of direct current.

6. Auxiliary coils for biasing or detenting are easily added to the main coil spool.

7. Field adjustment of sensitivity is readily accomplished by means of magnetic shunts.

8. Contact closure time is extremely short.

9. The armature may be arranged either to float or to remain closed to the contact last operated, such as in a memory relay.

Limitations

1. Since contact pressures in this type of relay are not large, an arc suppression circuit usually is necessary to prevent sticking or welding of the contacts.

2. To minimize the effects of vibration when the contacts are about to close or open, detenting of the contacts by one means or another is advisable.

3. Judicious location of the relay is necessary since the presence of other electrical fields, both direct and alternating current, may shift the calibration or induce chatter. Large pieces of iron or steel near the relay may have similar effects.

4. Although Nicaloi steel is used for the armature and pole pieces of the relay, a slight polarization or hysteresis effect results from impressing high voltages on the relay coil. This is removed readily by the subsequent normal reversals of current in the coil or by the application of a small a-c voltage to the coil.

5. Sudden shock, such as that resulting from rough handling or dropping, has been known to shift calibration slightly, but differential ordinarily remains unchanged in spite of severe treatment.

Although the limitations of the relay may appear to be somewhat formidable, it has been found by experience that to overcome them in many cases actually requires no more than good application engineering.

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No Discussion

Selective Tripping Arrangements for Protection of Ship's A-C Electric Distribution Systems

J. R. COLE
ASSOCIATE AIEE

THE NEW AIEE marine specifications just being published[1] call for the protective devices in shipboard a-c distribution systems to be arranged for selective operation so that a fault will be isolated with a minimum of interruption to service on the remainder of the distribution system. This paper reviews the method of achieving this end as employed on naval vessels and the tests conducted to demonstrate that selective operation of the protective devices function in accordance with the intended design on the completed installation. In order to discuss clearly the protective system on shipboard, the particular problems of shipboard installations are examined. It is considered that the new and novel feature brought out in this paper is the method of testing the system in order to demonstrate selective operation in accordance with the intended design.

Objective

Paragraph 53.09 of reference 1 states the following relative to the general design requirement for the protection of an a-c distribution system:

"*System Protection.* This should include providing suitable protective devices for each electric circuit and each unit of electric generating and power consuming equipment. The selection, arrangement, and performance of the various protective devices should be as recommended herein, and such as to provide complete and co-ordinated automatic protective arrangement having the following characteristics:

"(a) Maximum continuity of service under fault conditions through the selective operation of the various protective devices; that is, the isolation of a fault with the least interruption of service on the remainder of power distribution system, particularly those circuits affecting propulsion auxiliaries.

"(b) High-speed clearance of low impedance faults in order that fault currents of large magnitude will cause minimum damage to the system and equipment and minimize the hazard of fire.

"(c) Maximum protection of electric apparatus and circuits from damage under fault conditions, through the co-ordination of the electrical characteristics of the circuit or apparatus and the tripping characteristics of the protective devices."

The Bureau of Ships Specifications[2] state the following relative to the protection of power distribution systems:

"*General Design Requirements.* Each electric circuit and each unit of electric generating and power consuming equipment shall be provided with suitable protective devices as specified herein. The selection, arrangement, and performance of the various protective devices shall be such as to provide a complete and co-ordinated protective system having the following characteristics:

"High-speed clearing of all low impedance faults in order to minimize damage to the system and apparatus under fault currents of large magnitude.

"Maximum continuity of service under fault conditions through the selective operation of the various protective devices; that is, the isolation of any fault with the least interruption of service on the remainder of the power distribution system.

"Maximum protection of electric apparatus and circuits under fault conditions through the co-ordination of thermal characteristics of the circuit or apparatus and the tripping characteristics of the protective devices.

"Adequate interrupting capacity in all circuit interrupting devices or the provision of suitable back-up protection in such cases where the interrupting capacity of the device may be exceeded.

"Adequate thermal capacity in all the various circuit protective and switching devices for operation under normal loads, faults, and abnormal service conditions."

From a comparison of the two quoted general design requirements it may be seen that the ultimate end in view is essentially the same for both specifications.

Selective Tripping

Selective tripping is accomplished by the use of an inverse time delay on all circuit breakers that may be in a distribution system. This time delay is in the order of cycles and, as will be seen, is not always necessary for the protective devices that feed individual loads. To achieve selectivity, the time-current characteristics of each protective device must be such that for any possible fault current the time delay on a given circuit breaker exceeds the time delay and interrupting time of any circuit breaker that receives its power for distribution via the given circuit breaker (see Figure 2).

For an ideal selective tripping system each circuit breaker should be capable of interrupting the maximum available rms short-circuit current that may exist under any fault condition at the point in the distribution system where the protective device is located. If a protective device does not have sufficient interrupting capacity for the maximum available rms short-circuit current, it is necessary to provide back-up protection in the form of an instantaneous trip on the protective device that feeds the protective device that has insufficient interrupting capacity. This instantaneous trip is set to trip the so-called back-up circuit breaker instantaneously on all fault currents of magnitude greater than the interrupting capacity of the circuit breaker being backed up. Referring to Figure 2, if the circuit breaker represented by curve D has an interrupting capacity of only 15,000 amperes and the maximum available rms short-circuit current at the point in the distribution system where circuit breaker D is located is 40,000 amperes, it is necessary to set circuit breaker C to operate instantaneously on all fault currents above 15,000 amperes. Actually, because of manufacturing tolerances, the circuit

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J. R. Cole is a marine electrical engineer with the Bureau of Ships, Navy Department, Washington, D. C.

breaker is set to operate instantaneously at 13,500 amperes. The time current characteristic for circuit breaker *C* is represented by curve *CFI*. It may be seen readily from Figure 2 that this arrangement does not provide for selectivity between circuit breaker *C* and *D* under fault conditions where the magnitude of the fault is above 13,500 amperes.

Shipboard Problems in Obtaining Objective

The problems involved in shipboard distribution are considerably different from those involved for shore installations. Shore electric-power systems consist of a network of power stations linked together so that in case of temporary loss of a local power plant the local power demands will be supplied by other plants connected to the system. Further, the capacity of each plant is only a small percentage of the total generator capacity interconnected by the network. A ship is entirely dependent upon its own plant and in case of operating difficulties cannot look to outside activities to supply power or to assist in returning the plant to operation.

A typical 440-volt a-c shipboard power distribution system is shown in Figure 1. It is essentially a radial distribution system with bus tie circuits for connecting the different switchboards. There may be one or two generators feeding each switchboard and as many as eight switchboards. The sizes of these generators range from 60 to 2,000 kw. Therefore, in view of the relatively close coupling between the generators and the loads and the high decrements in the short-circuit currents, it may be seen that short-circuit currents of considerable magnitude have to be successfully interrupted. However, where distribution panels are located at considerable distance from the switchboard, or where a transformer is in the circuit or in some cases on ships with generators of low capacity, it is necessary to make sure that the minimum short-circuit current will be of sufficient magnitude to operate the instantaneous trip device on the type *AQB* circuit breakers.

The exigencies of shipboard service with the possibility of damage to relay circuits makes it undesirable to depend upon remote relaying circuits for selective operation. Therefore, the protective functions should be an integral part of the circuit breaker or by an overcurrent relay located adjacent to the circuit breaker.

The space and weight requirements for the electrical distribution system must be kept as small as possible on a naval vessel. As will be seen later on, this makes it necessary to feed individual loads from the type *AQB* circuit breakers with some sacrifice in selective operation where the available short-circuit current is above the interrupting capacity of the type *AQB* circuit breakers. This makes it necessary also to feed a power panel containing a number of type *AQB* circuit breakers, via a type *AQB* circuit breaker. Since the type *AQB* circuit breaker depends upon instantaneous trip for all low impedance faults, both the circuit breaker in, and the circuit breaker feeding, the power panel may be interrupted by a fault on a circuit emanating from the power panel.

On shipboard power systems, the locked rotor current of large induction motors may be as much as 60 to 100 per cent of the full load current of one generator. As a consequence, it is necessary that the time-current characteristics of the protection devices have a definite relationship to the time-current characteristics of the motor starting currents. The induction motors also have a transient under starting which is as much as 1.8 times the locked rotor current. In the application of type *AQB* circuit breakers for the protection of individual motor loads, it is necessary that the instantaneous overcurrent trip of the type *AQB* circuit breaker be set high enough to prevent the circuit breaker from being tripped on the motor starting transient.

In a manner similar to an induction motor, a transformer has an exciting current transient. It has been found that an instantaneous trip setting of 12 times the full load rating of a delta transformer bank is sufficient to prevent tripping of a circuit breaker feeding a transformer due to the exciting current transient.

Protective Devices Employed on Naval Vessels

The following types of protective devices are employed on naval vessels: type ACB circuit breakers; type AQB circuit breakers; fuses; and overload relays in motor controllers.

Type ACB Circuit Breakers

This is an air-quench type circuit breaker having an open metallic frame and one or more of the following protective functions and attachments integral with the circuit breaker generally employed for protection of the distribution system:

1. Long-time delay overcurrent protection.
2. Short-time delay overcurrent protection.
3. Instantaneous overcurrent protection.
4. Shunt trip.

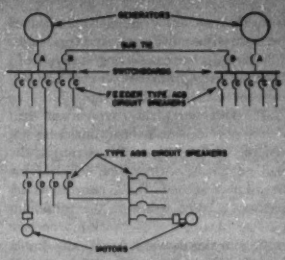

Figure 1. Schematic diagram of a typical ship's a-c power distribution system

The shunt trip on these circuit breakers may be operated by one or more relays with the following protective functions:

1. Reverse power protection.
2. Short-time delay overcurrent protection.
3. Long-time delay overcurrent protection.
4. Overpower protection.

It is desirable that the short-time delay overcurrent protection, the long-time delay overcurrent protection, and instantaneous overcurrent protection be integral with the circuit breaker for the reasons discussed previously and because of the problem of securing a suitable reliable source of tripping energy and the saving of space and weight due to the elimination of relays and associated equipment. The use of separate relays has been limited to short-time delay and long-time delay overcurrent protection on circuit breakers of 3,000-amperes, frame size or over when employed for generator or bus tie protection. In these cases, the exciter d-c voltage has been employed for the tripping energy.

The short-time delay overcurrent device provides inverse time delay at three different time settings, number 1, for minimum operating time (for use as feeder type *ACB* circuit breaker), number 2, for intermediate time (for bus tie protection), and number 3, for maximum operating time (for generator protection). The number-1 time-current trip band has a minimum time delay of three cycles (0.05 second) for any current up to 20 times the trip coil rating, but not exceeding 13,500 amperes. The number-3 time-current trip band has a maximum time delay of not greater than 24 cycles (0.40 second) for any current setting above 250 per cent of the pick-up current setting of the short time delay trip device. The number-2 trip band has time-current characteristics such that it will fit in between the number-1 and number-3 band in such a manner that there is a minimum

separation of one cycle (0.0167 second) between adjacent time-current trip bands. The pick-up current setting is capable of being adjusted from 200 per cent to 1,000 per cent of the trip coil rating.

The long-time delay overcurrent trip device provides an inverse time delay extending up to at least 800 per cent of the trip coil rating with a time delay of not less than 30 seconds at 125 per cent of the pick-up current value at the minimum pick-up settings. The pick-up current setting for this device is readily adjustable for any current from 150 to 300 per cent of the trip coil rating.

Curves *A*, *B*, and *C* on Figure 2 are typical type *ACB* circuit breaker time-current characteristic curves. Actually, the time-current tripping characteristics are bands instead of lines as indicated in Figure 2. This is a current band of approximately ± ten per cent of the mean. The type *ACB* circuit breakers are available in frame sizes with characteristics shown in Table I.

Type AQB Circuit Breakers

This is an air-quench type circuit breaker in a supporting and enclosing housing of insulating material having the following protective functions and attachments integral with circuit breaker; long time delay overcurrent protection; and instantaneous overcurrent protection.

Curve *D* in Figure 2 is a typical time-current characteristic curve for a type *AQB* circuit breaker. The long time delay is set to trip in 25 seconds ± 25 per cent at a current of 600 per cent of circuit breaker element rating, to trip in less than one hour at a current of 225 per cent of element rating and to trip in more than one hour at a current of 150 per cent of element rating. In standardizing the time-current characteristics of this long-time delay overcurrent protection, an effort has been made to provide a maximum protection for cable based on limiting the copper temperature rise to a value just below the temperature at which the insulation would be damaged. These circuit breakers are standardized in frame sizes based on maximum continuous current-carrying capacity. Table II shows the interrupting rating and approximate dimensions of the different frame sizes. Standardized element ratings are provided for these circuit breakers. The standard elements are manufactured with two different instantaneous trip ratings, one set to trip at 600–700 per cent of the element rating and the other, in most cases, set to trip at 1,200–1,400 per cent of the element rating.

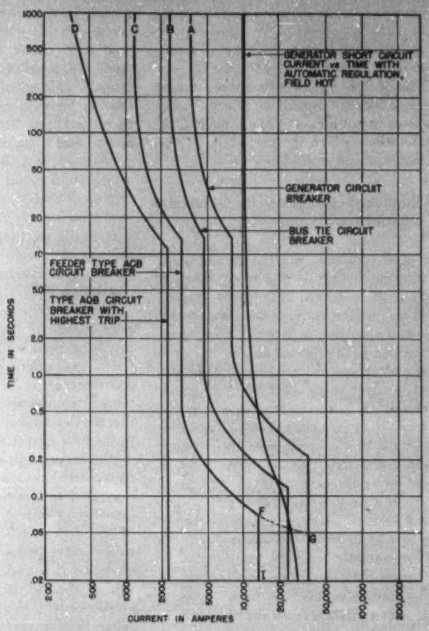

Figure 2. Time-current characteristics of circuit breakers in Figure 1

Fuses

When employed for protection, fuses are generally used in conjunction with a switch. However, because of their limited interrupting capacity and other considerations discussed previously,[3] their use is generally limited to lighting circuits and nonvital power circuits.

Overload Relays in Motor Controllers

The function of this relay is to furnish overload protection for a given motor and therefore does not enter into the problems of system protection as discussed herein

Arrangement of Devices to Obtain Selectivity

As stated previously selective operation is achieved by the provision of protective devices that have time-current characteristics arranged so that the time delay of any given circuit breaker exceeds the time delay and interrupting time of any circuit breaker that receives its power for distribution via the given circuit breaker. The protection afforded by circuit breakers is essentially intended as fault protection and is not designed to give overload protection. The overload relays in the controllers are depended upon for overload protection of motors, and the operator is depended upon to take care of any overload on the generators.

Table I. Minimum Interrupting Rating, Short Time Thermal Capacity, and Dimensions of Type ACB Circuit Breakers

Frame Size, Amperes	Maximum Interrupting Rating, Amperes	Short Time Thermal Capacity, Amperes	Approximate Size of Circuit Breakers in Inches			Weight Each, Pounds
			Height	Width	Depth	
225	20,000	3,500	17	12	8¼	61
600	40,000	18,000	24	13	13	95
1,600	60,000	24,000	27	22	16	260
3,000	75,000	36,000	35	34	22	600
4,000	100,000	50,000	39	35	22	635

Table II. Maximum Interrupting Rating and Dimensions of Type AQB Circuit Breakers

Frame Size, Amperes	Maximum Interrupting Rating, Amperes	Approximate Size in Inches			Weight Each, Pounds
		Height	Width	Depth	
100	15,000	10	5	4 1/16	9 1/4
225	20,000	15 1/4	9	4 15/16	26
600	30,000	22	9	5 1/4	60

Some degree of overload protection is afforded by the long time delay overcurrent protection of the circuit breakers; however, the primary function of the long time delay overcurrent protection is to provide high impedance fault protection.

Until the magnitude of the short-circuit current is known, it is not possible to apply protective devices in a manner to assure adequate protection. The methods of calculating the magnitude of short-circuit currents in low-voltage distribution systems are established and will not be discussed here; however, the following is a list of short-circuit calculations that are necessary for the correct application of protective devices:

1. *Maximum Available rms Short-Circuit Current (Average of Three Phases).*

This current which is the basis for interrupting rating of low voltage (600 volt or less) air circuit breakers, is the average value of the maximum available rms short-circuit currents which occur in the three phases during the first cycle after inception of the short circuit. This calculation is based upon the maximum expected parallel generator capacity plus the contribution of the maximum induction and synchronous motor load connected to the system under this condition. In making these calculations, all generators and significant circuit impedance up to the line terminals of the circuit breaker under study are considered.

2. *Maximum Available rms Asymmetrical Short-Circuit Current.*

This current which is the basis for the interrupting rating of fuses, and the short time current rating and mechanical strength of switches, cables, bus work and circuit breakers, is the total rms current at one-half cycle in the phase carrying the maximum asymmetrical current. This calculation is based upon the maximum expected parallel generator capacity plus the contribution of the maximum induction and synchronous motor load connected to the system under this condition. In making these calculations, all generator and significant circuit impedances up to the line terminals of the device under study are considered.

3. *Minimum Available rms Asymmetrical Short-Circuit Current at the Point of Application of Each Circuit Breaker.*

This quantity is required to determine that the trip settings of all the protective devices are within the available fault current under all conditions of operation. This current is determined on the basis of one generator of the lowest rating connected to the systems under consideration. It is assumed that the contribution of the induction and synchronous motorload is negligible. The fault is considered to be located on the line side of the next circuit breaker beyond the one in question and the total impedance to the point of fault includes the resistance and reactance of the circuit breaker in question.

4. *Maximum rms Asymmetrical Short-Circuit Current of Each Generator.*

This quantity is required with regard to the pickup current setting of the instantaneous over-current trip device on the generator circuit breaker. This current is determined on the basis of the generator being short circuited from rated load at rated power factor. This short circuit is assumed to be located on the line terminals of the generator circuit breaker.

5. *Maximum Sustained rms Short-Circuit Current of Each Generator.*

This quantity is required with regard to the pick-up current setting of the short time delay overcurrent device of the generator circuit breaker. It is determined on the basis of a short circuit at the line terminals of the generator circuit breaker and includes the effect of the generator voltage regulator with field hot.

For the purpose of this study the circuits that require protective devices may be divided into four categories as follows: single load circuits; feeder circuits; bus tie circuits; and individual generator circuits.

Protection of Single Load Circuits

The single load circuits are those circuits supplying power to a single unit of power consuming equipment (motor, transformer, heater, and so forth). The type *AQB* circuit breaker is employed for the protection of these circuits except that in certain cases fused switches are used for the protection of nonvital loads. These fused switches are limited to nonvital loads where the fuse size does not exceed 60 amperes and the maximum available rms asymmetrical short-circuit current does not exceed the interrupting capacity of the fuse. The interrupting capacity of fuses varies from 3,000 to 8,000 amperes depending upon the fuse size. When fuses are installed in large capacity systems, they are always fed via a type *AQB* circuit breaker to insure adequate isolation of heavy faults on circuits protected by fuses. For noninductive loads, fuse sizes are selected at approximately 125 per cent full load current of the circuit. For motor loads fuse sizes are 2/3 locked rotor current. Navy controllers as a rule do not employ fuses.

The type *AQB* circuit breaker which is employed in most cases for the protection of individual load circuits is provided with long-time delay overcurrent and instantaneous overcurrent protective features. The element rating and the instantaneous overcurrent trip setting must be taken into consideration when applying type *AQB* circuit breakers for the protection of individual load circuits. The element rating is equal to, or the next size above, the full load current of the circuit. As pointed out in the section, "Protective Devices employed on naval vessels," the type *AQB* circuit breakers are standardized with a selection of two instantaneous overcurrent trip settings for each element rating; that is, 600-700 per cent of element rating and 1,200-1,400 per cent of element rating. The instantaneous overcurrent trip is selected to meet the following requirements:

1. So it will exceed the maximum inrush current of the circuit; that is, the induction motor starting current transient or transformer exciting current transient. It has been found that an instantaneous overcurrent trip setting of 1,200 per cent of the circuit-breaker element rating when selected in accordance with the foregoing will provide an instantaneous trip above the transient current of an induction motor or a transformer. Where reduced voltage closed transition starting is used, the instantaneous overcurrent trip setting may be reduced in proportion to the reduction in the steady-state starting current taken from the line.

2. In order to assure that the circuit breaker will trip instantaneously under short-circuit conditions, the instantaneous overcurrent trip setting of the circuit breaker should not be greater than 60 per cent of the minimum available rms short-circuit current at the point of application of each circuit breaker.

3. The instantaneous overcurrent trip setting is selected as low as is compatible with the foregoing requirements using standard trip settings so as to have the greatest band between this circuit breaker and the time-current characteristics of the circuit breaker feeding this circuit breaker.

In some instances, the minimum and maximum requirements for the instantaneous overcurrent trip settings have been found to be somewhat incompatible. However, in general, a satisfactory arrangement can be realized by a suitable arrangement of the circuits involved.

Protection of Feeder Circuits

Feeder circuits are circuits supplying power to a group of loads (two or more). These circuits may emanate from a load-center power-distribution panel or from a generator and distribution switchboard and are supplied through either type *AQB* or type *ACB* circuit breakers.

Type AQB Circuit Breakers. All feeder circuits emanating from load-center power distribution panels are supplied through type *AQB* circuit breakers. Where the maximum available rms short-circuit current is within the interrupting rating of a type *AQB* circuit breaker or where adequate back-up protection is provided in the form of a type *ACB* circuit breaker, the type *AQB* circuit breaker may be used to supply feeder circuits emanating from the generator and distribution switchboard. In all of the preceding applications, the type *AQB* circuit breaker is provided with long-time delay and instantaneous overcurrent protective features. The element rating and the instantaneous overcurrent trip setting must be taken into consideration when applying type *AQB* circuit breakers in this manner. The element rating is equal to or next in size above the resultant load current of the circuit. The instantaneous overcurrent trip is selected to meet the following requirements:

1. It should exceed the arithmetical sum of 1.6 times the steady-state starting current of the largest motor plus the full load currents of the balance of the loads on the circuit so as to prevent the possibility of the circuit breaker being tripped falsely under normal operating conditions.

2. In order to assure that the circuit breaker will trip instantaneously under all short-circuit conditions, it must not be greater than 60 per cent of the minimum available rms short-circuit current at the point of application of each circuit breaker.

3. It should be such that it will co-ordinate with the time-current characteristics of the circuit breaker feeding this circuit breaker and with any circuit breaker being fed by this circuit breaker.

The employment of the type *AQB* circuit breakers for the protection of feeder circuits results in the installation of two type *AQB* circuit breakers in series, both of which depend upon instantaneous overcurrent trip for protection against short-circuit faults. In a great many cases the magnitude of the available short-circuit current on a fault in a circuit protected by two type *AQB* circuit breakers in series will be greater than the instantaneous overcurrent setting of both devices. Accordingly, there is little likelihood of selectivity between these devices. Because of space and weight considerations, it is necessary to use type *AQB* circuit breakers for the protection of group feeders. However, every effort is made to limit the type *AQB* circuit breakers in series to two, and to group the loads on feeders so as to limit the interrupting of like services that might result from a fault on a single load circuit interrupting the group feeder circuit breaker also.

Type ACB Circuit Breakers. All feeder circuits emanating from generator and distribution switchboards where the maximum available rms short-circuit current exceeds the interrupting rating of a type *AQB* circuit breaker are supplied through type *ACB* circuit breakers having the required interrupting rating. In some cases, where the space and weight limitations of the vessel will permit, type *ACB* feeder circuit breakers are employed because of the additional selective protection that they afford even though they are not required for back-up protection. In the application of type *ACB* circuit breakers for the protection of feeder circuits, the circuit breakers are provided with long-time delay, short-time delay and instantaneous overcurrent protection.

The long-time delay overcurrent protection is to provide a maximum degree of high impedance fault protection. To achieve this, the long-time delay device is set to pick-up at approximately 150 per cent of the resultant load current on the circuit, and the time-current characteristics of the long-time delay are selected such as to exceed the time-current characteristics of the maximum normal current inrush of the circuit.

The function of the short-time delay overcurrent protection is to provide for high clearing of all faults on the feeder circuits, and to provide selective operation between the type *ACB* feeder circuit breakers and the type *AQB* circuit breakers at the power-distribution panel supplied by the feeder circuit. This protective device is generally set to pick-up at not less than 120 per cent of the highest instantaneous overcurrent setting of any type *AQB* circuit breaker connected to the feeder circuit protected by the given type *ACB* circuit breaker in order to assure selectivity with the type *AQB* circuit breaker. However, it may be seen from a study of the curves on Figure 2 that a large degree of selectivity is realized if the instantaneous trip of the type *AQB* circuit breaker is above the pick-up of the short time delay on the type *ACB* breaker; that is, nonselectivity would result only for fault currents of the values of the band of overlap of the instantaneous trip of the type *AQB* circuit breaker and the pick-up of the short-time delay device on the type *ACB* circuit breaker. In order to eliminate the possibility of the feeder circuit breaker tripping under inrush current conditions, the short time delay is set to pick-up at not less than 125 per cent of the maximum normal inrush current of the feeder circuit. In order to provide selectivity with the bus tie circuit, the short-time delay device should be set to pick-up at not more than 80 per cent of the pick-up of the short-time delay device on the bus-tie circuit breaker. As discussed in the section on protective devices, the time-current characteristics of the short-time delay overcurrent device is such as to give a minimum time delay of 0.05 second on all current values up to the interrupting rating of the type *AQB* circuit breaker, in order to assure that this device will not trip the type *ACB* circuit breaker before a fault is cleared by the type *AQB* circuit breaker.

Instantaneous overcurrent protection is provided in the feeder type *ACB* circuit breaker where the interrupting rating of any of the type *AQB* circuit breakers is less than the maximum available rms short-circuit current and/or where the maximum available rms asymmetrical short-circuit current may exceed the short time rating of the type *ACB* feeder circuit breaker. When required as back-up protection for type *AQB* circuit breakers, the instantaneous overcurrent device is set to trip at 90 per cent of the interrupting rating of the smallest type *AQB* circuit breaker connected to the feeder. Where required, because the maximum available rms asymmetrical short-circuit current exceeds the short time rating of the circuit breaker, the instantaneous overcurrent trip is set to trip at the short time rating of the circuit breaker. If required for both of the foregoing reasons, the instantaneous trip is set to trip at the lower of the above two values.

Protection of Bus Tie Circuits

Type *ACB* circuit breakers provided with long-time delay overcurrent, short-time delay overcurrent, and sometimes instantaneous overcurrent protection are provided for the protection of bus tie circuits.

The function of the long-time delay overcurrent protection is to provide a maximum degree of high impedance fault protection on the bus tie circuit. The pick-up setting of the device is set at approximately 150 per cent of the resultant load current of the circuit; however, it must be not less than 123 per cent of the

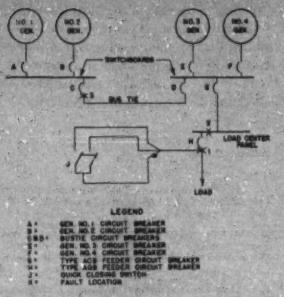

Figure 3. Selective tripping demonstration procedure

highest pick-up current setting of the long-time delay overcurrent trip device on the feeder circuit breaker and not more than 80 per cent of the pick-up current setting of the long-time delay overcurrent device on the generator circuit breaker, in order to assure selectivity with the feeder circuit breaker and the generator circuit breaker.

The function of the short-time delay overcurrent protection is to provide for the high-speed clearing of all faults on the bus tie circuit and the switchboard busses connected thereto and selective operation between the bus tie circuit breaker and the feeder circuit breakers for all faults on feeder circuits. This protective device is set to pick-up at a current of not more than 80 per cent of the pick-up current setting of the generator circuit breaker. As discussed in the section on protective devices, the time-current characteristics of this protective device are such that for any value of current in excess of the pick-up current setting the time delay of the bus tie circuit breaker exceeds the sum of the time delay and interrupting time of any feeder circuit breaker.

Instantaneous overcurrent protection is provided on bus tie circuit breakers only when the maximum available rms asymmetrical short-circuit current exceeds the short time rating of the bus tie circuit breaker, in which case, it is set at the latter current value.

Protection of Generators

Ship's service generators are connected to the ship's service distribution system through type *ACB* circuit breakers provided with long time delay overcurrent, short time delay overcurrent, and, in most cases, instantaneous overcurrent protection and reverse power protection.

Long time delay overcurrent protection is provided to prevent excessive temperature rise of the generator on extreme conditions of overload only. Since most ship's service generators have a load capacity of 150 per cent of full load for five minutes, this device is set to pick-up on a current approximately equal to but not less than 150 per cent of the generator full load current. The time-current characteristics of this device are such as not to exceed the normal inrush current to be supplied by the generator.

The function of the short time delay overcurrent protection is to provide for the high-speed clearing of all faults in the generator switchboard and for selective operation between the generator circuit breaker and the bus tie and feeder circuit breakers for all faults. This protective device is set to pick-up at a current value of not more than 80 per cent of the maximum sustained rms short-circuit current of the generator. As stated previously, the time delay of this device for any value of current above 250 per cent of the pick-up current is not greater than 24 cycles (0.40 second). Further, in the application of this device on a generator circuit breaker, the time delay is set such that, for any value of current in excess of the pick-up current setting, the time delay of the generator circuit breaker is greater than the sum of the time delay and interrupting time of the bus tie circuit breaker at the corresponding value of current.

Instantaneous overcurrent protection is provided whenever the short time rating of the generator circuit breaker may be exceeded in the particular application and/or when more than two generators may be operated in parallel. When provided, this device is set to trip at a current approximately equal to but not less than 120 per cent of the maximum rms asymmetrical short-circuit current of the generator. The short time ratings of the standard type *ACB* circuit breaker are such that 120 per cent of the maximum rms asymmetrical short-circuit current of a generator will not exceed the short time rating of a generator circuit breaker. This instantaneous overcurrent protection is provided to isolate the generator and generator cable in case the generator cable is short circuited. Under these circumstances, the fault current for tripping the protective device is supplied by the motor contribution and the other generators in the circuit.

Reverse power protection is provided on all generators that are required to operate in parallel with other power sources. The purpose of the reverse power protection is to prevent damage to the prime mover in case of loss of prime mover power and, in addition, to clear faults in the generator and generator cables. This protection is provided by means of a relay that operates the generator circuit-breaker a-c shunt trip. In order to prevent the synchronizing power transients from causing false operation, the reverse power relay is provided with inverse-time-power characteristics such that when energized at rated voltage the relay will trip in approximately ten seconds with reverse power equal to five per cent of the generator rating.

Shipboard Tests

After the distribution system is installed aboard the vessel, tests are conducted to demonstrate selective operation in accordance with the intended system design. These tests are conducted with a ship's generator furnishing the fault current. A bolted fault is simulated by a quick-acting switch. Figure 3 shows the connections for a complete set of tests on one switchboard, and Table III is a tabulation of a complete set of tests for one switchboard. The minimum number of tests conducted are as follows:

1. Test for selectivity between each generator circuit breaker on each switchboard and each bus tie circuit breaker connected to that switchboard (demonstrations 5 and 6 in Table III).

2. Test for selectivity between the circuit breakers in each bus tie feeding each switchboard and a feeder type *ACB* circuit breaker on that switchboard (demonstration 4 in Table III). The feeder circuit breaker with the highest short time delay overcurrent pick-up setting is selected for this test on each switchboard.

3. Test for selectivity between each generator circuit breaker on each switchboard and a feeder type *ACB* circuit breaker connected

Table III. Selective Tripping Demonstration Procedure

Demonstration Number	Fault Location	Circuit Breakers Closed Before Demonstration	Operate Generator Number	Demonstrates Selectivity Between
1	1	F, G, H	4	H and G
2	2	F, G	4	G and F
3	2	E, G	3	G and E
4	2	B, C, D, G	2	G and D or C
5	3	F, D	4	D and F
6	3	E, D	3	D and E

to the switchboard being tested (demonstrations 2 and 3 in Table III). It is obvious that this test may be omitted in all cases where demonstrations 4, 5 and 6 adequately prove selectivity between generator and bus tie and between bus tie and feeder type *ACB* circuit breakers.

4. Test to demonstrate selectivity between a feeder type *ACB* circuit breaker and a feeder type *AQB* circuit breaker on each switchboard (demonstration 1 in Table III). The combination of feeder type *ACB* and type *AQB* circuit breaker selected for this test on each switchboard is the one that comes closest to having overlapping time-current characteristics.

It is evident that on vessels which have the type *AQB* feeder circuit breakers connected directly to the switchboard bus the test is modified accordingly to suit the actual designed distribution arrangement.

These tests are conducted with only one generator supplying the fault current and the fault is applied when the generator is operating at no load. It is recognized that there is a wide range of possible fault currents that the circuit breakers may be called upon under actual operating conditions to interrupt selectively depending upon the number of generators in operation and the motor load being fed by the generators. Accordingly, the best that the shipboard test can do is to demonstrate selectivity at one magnitude of fault current. In most cases this fault current is of the magnitude required to cause the type *ACB* circuit breakers to operate in the short time delay overcurrent band. Since it can be assumed that each circuit breaker will have essentially the same slope of its time-current curve, this test can be considered an adequate demonstration of selectivity.

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Discussion

J. W. Simpson (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The author's paper is an excellent review of the subject. The fact that the Navy has several vessels in operation with systems embodying essentially the same protection system described, lends considerable weight to the statements made in this paper as operating experience is the acid test for any protective system.

I would like to take this opportunity to make several comments which occur to me. It is important to remember that the current time characteristics of circuit breakers are bands rather than lines. The band furthermore is not the difference between the maximum and minimum operating times. The upper limit of the band is the maximum operating time of the circuit breaker and the lower limit is the reset time, that is the maximum time the circuit breaker will carry the given current and still reset when the current is suddenly reduced to the continuous current rating of the circuit breaker.

It should also be kept in mind that when circuit breakers can only open after a time delay both thermal and mechanical stresses become much more important. In some applications, the short time rating should be equal to the interrupting rating of the circuit breaker. In general for shipboard application the 1,600-ampere frame circuit breaker should have a short time rating of 60,000 amperes, whereas the 600-ampere frame need only have a short time rating of 18,000 amperes or 30 times coil rating. This is due to the fact that the interrupting rating of the 600 ampere is proportionately higher and to the fact that no more than two generators ever feed through a 600 ampere bus tie breaker. Generator circuit breakers need never have instantaneous trips set higher than 120 per cent of maximum assymetrical rms short circuit current of one generator. Feeder breakers need never have their instantaneous trips set higher than 1,200 to 1,400 per cent of coil rating or 13,500 amperes. In the overload range circuit breakers definitely are not selfprotecting. For example if the long-time delay pickup is 300 per cent, the circuit breaker can be damaged by a current of 250 per cent of its rating flowing continuously.

Cole states that the instantaneous trip setting of *AQB* circuit breakers should not be greater than 60 per cent of the minimum available rms short-circuit current at the point of application. This seems to be a good compromise, however, if there is any appreciable fault impedance the circuit breakers still may not trip instantaneously.

Cole states that if a protective device does not have sufficient interrupting capacity for the maximum available rms short-circuit current, back-up circuit breakers should be provided. I believe that there should be a limit to the disparity in interrupting rating between the back-up and feeder circuit breakers. For example a circuit breaker having an interrupting capacity of 100,000 amperes probably would not provide much back-up protection for a 15,000 ampere interrupting rating circuit breaker. The exigencies of shipboard installation might make it desirable, however, to permit a somewhat greater ratio than the two-to-one required by present National Electrical Manufacturers Association rules.

In the design of protective systems as well as in the judging of their performance, it must be remembered that circuit breakers with no overlapping in their tripping bands may still not be selective if the circuit breaker with the longer desired tripping time should be carrying additional parallel load. This is particularly true where the tripping curves are very inverse.

In connection with the shipboard tests described by Cole, it might be well to raise the question as to what is the real objective of such tests. True, they give an indication of adequate selectivity, but they are far from comprehensive enough to be conclusive and therefore may be misleading, in that they give false assurance of a satisfactory protective system.

J. R. Cole: The first four paragraphs of Simpson's comments deal with the limitations of circuit breakers. The selective tripping as discussed in my paper is based on the application of these devices with their inherent limitations. As pointed out in my paper, these limitations in the protective devices, such as the short-time thermal capacity of type *ACB* circuit breakers mean some compromise in the actual resultant selective operation. As stated under "Arrangement of Device to Obtain Selectivity," the primary function of the long-time delay over-current protection is to provide high-impedance fault protection, not overload protection as implied in paragraph 4 of Simpson's remarks. It is realized that one of the limitations of the type *ACB* circuit breaker is its short-time thermal capacity. Steps are presently being taken to increase this thermal capacity without increasing the inherent operating time of the circuit breaker.

If the fault impedance on a circuit protected by a type *AQB* circuit breaker is of such magnitude, that the fault current will not trip the circuit breaker instantaneous trip when the circuit breaker is applied in accordance with the rules set forth in my paper, the circuit breaker thermal trip must be depended upon for circuit protection. Experience shows that this will seldom happen in shipboard installations when the circuit breaker instantaneous trip is not greater than 60 per cent of the minimum available rms short-circuit current at the point of application of each circuit breaker.

Simpson states that "it must be remembered that circuit breakers with no overlapping in their tripping bands may still not be selective if the circuit breaker with the longer desired tripping time should be carrying additional parallel load." This might be true if there were no motors in this parallel load. In shipboard applications, this parallel load will consist at least partially of motors. These motors will contribute partially to the fault current, thus lessening the current on the parallel circuit and increasing the fault current through the circuit breaker protecting the circuit which has the fault.

As stated, the shipboard selective tripping demonstrations do not attempt to duplicate all possible fault conditions or possible magnitudes of fault current or to check the calibration of the protective devices. These tests do demonstrate selectivity under certain possible operating conditions, they do indicate certain limitations, and they serve to provide information for future designs.

Ventilation of Rotating Electrical Equipment on Shipboard

A. M. HARRISON
MEMBER AIEE

ONE of the most important limitations to the capacity of electric machinery is the temperature of the windings. The temperature limit for electric machines depends on the class of insulation employed. The maximum hot-spot temperature limits of 105 degrees centigrade and 130 degrees centigrade for class *A* and *B* insulation respectively are well established. The limit for silicone insulation is not so definitely fixed, but 180 degrees to 200 degrees centigrade is being considered for hot-spot temperature.

There are many limitations other than temperature to be considered in the design of electric machinery. Some of these are power factor, efficiency, pull-out torque, overload capacity, commutation, and regulation. However, with class *A* and *B* insulation, temperature rise is one of the first limitations to be reached, so the problem of ventilation is of extreme importance. The use of silicone insulation has raised the temperature limits to the point where other limitations may occur first but good ventilation is still essential to obtain the best balanced design.

The importance of properly ventilating machines cannot be overemphasized. The more efficient the ventilation system can be made, the greater will be the output which can be obtained from a given amount of material, while still maintaining the temperature within proper limits. This, of course, is the ultimate aim of the designer and is extremely important on shipboard where space is usually at a premium.

It is the purpose of this paper to describe some of the methods used in ventilating various types of electric machinery.

When the temperature rise of a machine becomes constant, it means the heat is being absorbed by the cooling medium at the same rate at which it is generated. The rate at which heat is absorbed by the cooling medium is a function of the watts per square inch of exposed surface, the average temperature difference between the cooling medium and the surface temperature, and the velocity at which the cooling medium passes over the exposed surfaces. All of these factors are quite variable throughout the machine and it would be extremely difficult to determine the temperature rise for each individual design from these considerations alone.

The best possible means of determining the proper amount of air required for a given temperature rise is from tests of previously designed similar machines. It has been found as the result of many tests that on the average for 40 degrees centigrade rise machines, approximately 135 cubic feet per minute per kilowatt loss is required. This gives about 13 degrees rise of cooling air, using the relation that

$B = 1.765W/\theta_a$ where

V = volume of air in cubic feet per minute
W = watts loss to be absorbed by air
θ_a = temperature rise of air in degrees centigrade

Similarly, it has been found that for 70 degrees rise machines approximately 70 cubic feet per minute per kilowatt loss is required and for 85 degrees rise about 55 cubic feet per minute per kilowatt loss. These values correspond to rises of 25 degrees centigrade and 32 degrees centigrade in the cooling air respectively. It should be noted that these are approximate values only and may vary ±10 per cent depending on the efficiency of the ventilating system. The volume of air required is not inversely proportional to the temperature rise due to the fact that the surface heat transfer varies about as the 8/10 power of the air velocity.[1]

The foregoing values of air volume per kilowatt loss are used when machines are force ventilated. When machines are self-ventilated, the air volume circulated through the machine is fixed by the speed. Obviously self-ventilated machines must be designed so the losses can be dissipated with the natural air volume and still not exceed the temperature limits.

To circulate the cooling medium in the most efficient manner, rotating electric machines are designed to take advantage of the natural fan action of the rotating element. Electric machines are constructed usually of laminations to reduce core losses and this type of construction lends itself very well to radial ventilation. By building the iron in packages with vent spacers at regular intervals, radial ducts are formed. This increases very materially the area of the core in contact with the cooling medium and the area available for dissipating heat.

It is a well known fact that the heat conductivity along the laminations is about 50 times as great as the conductivity across the laminations. This would seem to indicate that axial ventilation, where air is blown across the ends of the laminations, would be better than radial ventilation. However, this is not true because the radial vents make better use of the fan action of the rotor. Also, with proper sized packages of laminations, the area of the ducts is so much greater than the circumferential area and the length of the path for the heat to travel is so much smaller, that a very large portion of the heat is conducted across the laminations. The heat is dissipated in the ducts in spite of the poorer heat conductivity across the laminations. The greater the radial depth of punchings, the more nearly this is the case because the circumferential

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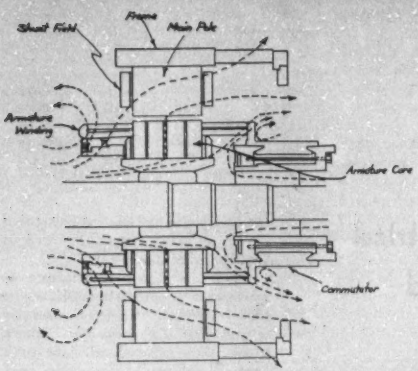
A. M. HARRISON is with the Westinghouse Electric Corporation, East Pittsburgh, Pa.



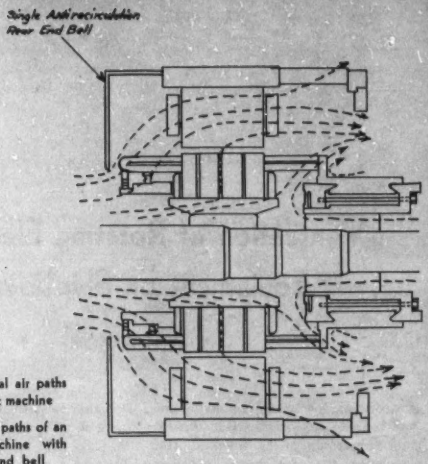


Figure 1 (left). Natural air paths of an open type d-c machine

Figure 2. Natural air paths of an open type d-c machine with antirecirculation end bell

area remains almost constant, regardless of the punching depth; whereas, the duct area increases with increase in the depth of laminations.

Improved ventilation of machines can be obtained by directing the air so it is most effective. This can be done by designing baffles and end bells. Blowers are frequently used either as an integral part of the rotor or, as in the case of slow speed machines, the blower may be separately driven and external to the machine with suitable ducts for directing the air into and out of the machine.

The natural paths of the air flow for a simple open type d-c machine are shown in Figure 1. The end windings at the rear act as a radial blower, as do the ducts in the armature. If nothing is done to prevent it, some of the air recirculates and is again drawn into the rear-end winding. This makes the end windings hotter than necessary. By adding a single rear-end bell as shown in Figure 2, this recirculation can be prevented with a resultant decrease in temperature rise. The commutator necks act as a very effective blower and tend to pull air through the machine under the core from rear to front. The commutator necks also pull air across the commutator surface and so help to cool the commutator. This is shown in Figure 1.

Additional fan action can be obtained by installing blowers under the armature coils at the front and rear. Additional cooling can be obtained for the field windings by installing blowers at the extreme end of the armature windings and extending the rear-end bell slightly. Additional cooling of the commutator is accomplished by adding short vanes on the extreme front end of the commutator. The big advantage of the commutator vanes is not so much the increased blower effect, but the greatly increased radiating surface available for dissipating the commutator losses. With vanes about $1^1/_2$ inches wide and 2 inches in radial depth, the radiating surface per bar is more than doubled. Blowers can be brazed also to the necks to increase the radiating surface and thus lower the temperature rise. See Figure 3.

Shaft-mounted fans are sometimes used to obtain increased output when the natural ventilation is restricted by such things as drip-proof covers, semienclosing covers, splashproof machines, and so forth. Attached fans are used also when machines are totally enclosed using recirculated air and a heat exchanger. In such cases it is necessary for the attached fan to develop only the additional pressure required to take care of the pressure drop in the duct work and in the cooler since, on high-speed machines, the armature develops enough pressure to cool itself if the machine did not have any external restrictions. Obviously, attached fans are used only on machines where the speed is high enough to make a blower of this type effective. Where the rotation speed of the armature is not high enough to give a good blower design, the benefits of additional ventilation can be secured by using a separate motor driven blower. This type blower can be used either with a recirculated system, or in a system where the air is taken from the outside of the machine and discharged into the open after being forced through the machine by the blower. Figure 4 shows a slow-speed d-c dredge pump motor with a separate motor driven blower. The air is forced into the rear and discharged at the front. The front end is equipped with a drip-proof cover.

When attached fans are used, the fan may be mounted at the front or the rear of the machine. By mounting the fan at the front on d-c machines, the air paths are exactly the same as in the self-ventilated machine or in the natural direction of the air flow. In addition, the carbon dust from the commutator and brushes is drawn into the fan and not into the commutator risers and windings as is the case when the fan is at the rear of the machine. The length of the machine is about the same whether the fan is at the front or rear. With the fan at the front end, the commutator is cooled by air which is heated by the losses in the armature. Due to the fact that practically all the ventilating air is made to pass over the commutator, the temperature rise is about the same as the reverse air flow where the air temperature is lower but the volume of air is less. These two schemes of ventilation are shown in Figures 5 and 6. Locating the fan at the rear end of the armature has the advantage of giving a little better brush accessibility on small machines. On larger machines very good brush accessibility can be obtained even with the fan on the front end, as openings in the fan spokes can be used for getting at the brushes from the front, as well as the circumferential openings on the sides.

With a nonrecirculated system, the air

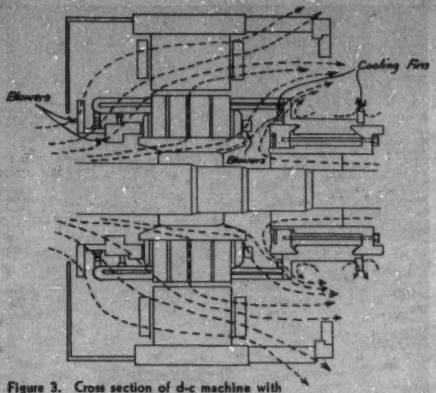


Figure 3. Cross section of d-c machine with internal blowers on armature and cooling fins added to back of commutator necks and front of commutator bars for additional cooling

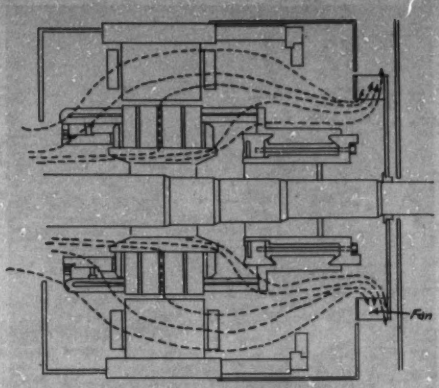


Figure 5. Cross section of a d-c machine with fan at front, giving forward direction of air flow

is discharged radially all around the machine as shown in Figure 5. In the case of a recirculated system with a cooler, it is necessary to use a housing, preferably of the volute type, so all the air discharged from the blower can be collected efficiently and directed through the cooler. Such a ventilation scheme is shown in Figure 7. The cooler shown is of the double-tube type so a leaking tube will not allow water to get into the ventilating system. If the cooler is mounted away from the machine, coolers of the single-tube type of construction may be used.

If machines with a recirculatory system of ventilation have no filter, the carbon dust worn from the brushes will deposit on the windings, internal creepage surfaces of the machine, air ducts, and coolers. The advantage of the forward direction of air flow through the machine from rear to front is apparent in this case as a large portion of the dust will be deposited on the cooler and duct work before reaching the vulnerable front end of the machine. Back of the commutator necks is a very bad spot because it is a very difficult location to clean. Mechanical filters of the filter paper, glass wool or impingement type, while not completely effective, are very helpful in preventing the recirculation of brush dust. Electrostatic-type filters, such as the Precipitron, are very efficient in this respect but,

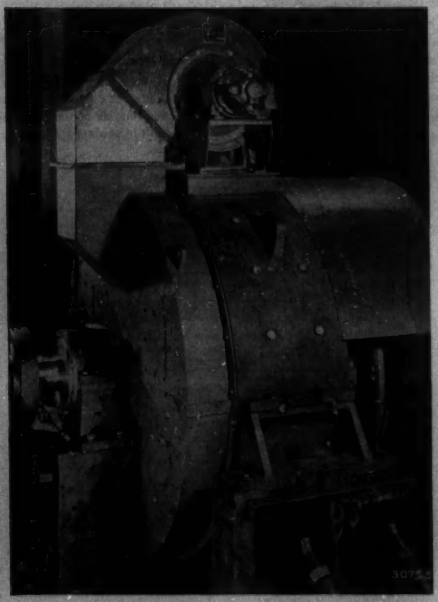
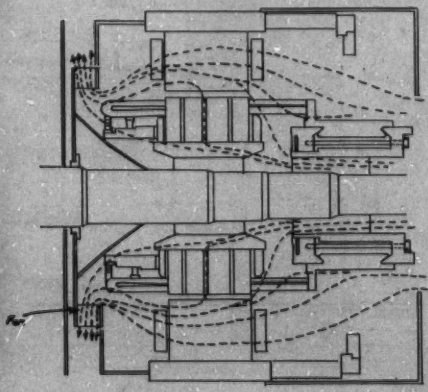


Figure 4 (left). D-c dredge pump motor with separate motor driven blower

Figure 6. Cross section of a d-c machine with fan at rear, giving reverse direction of air flow



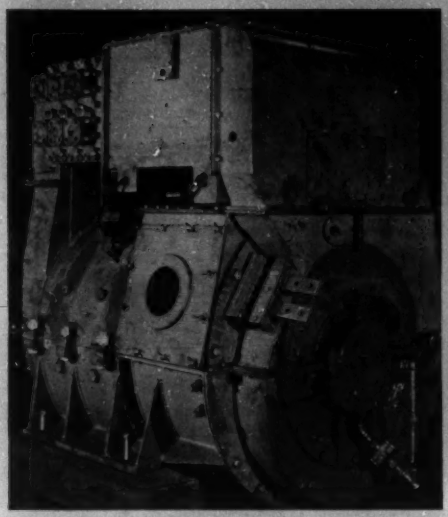


Figure 7. D-c generator with recirculated air cooling system, consisting of attached fan, volute housing, precipitron, and cooler

Figure 9. Shipboard installation showing suction-type ventilation

due to the low frontal velocity required, are not very practical as a built-in part of the ventilating system for large machines, unless the space requirements are not limited. Figure 7 shows a practical installation of an electrostatic-type filter where the frontal area required is no larger than that required for the cooler. This machine is approximately 400 kw at 1,200 rpm.

If a machine is installed in a very oily atmosphere it may be necessary to take special precautions even when the machine is totally enclosed with a recirculating air system. There is always a certain amount of leakage in such a system and if nothing is done to prevent it, the atmosphere inside the machine will eventually have the same concentration of oil vapor as exists outside. A scheme which has been used successfully, is to open the machine to the outside through a Precipitron at lowest point of pressure in the enclosed system. This means that all other points in the system are above atmospheric pressure and the leakage therefore is outward at all points except the point where the air can enter through the Precipitron.

The filter will remove any oil vapor

Figure 8. Suction ventilation from front anti-recirculation rear end bell

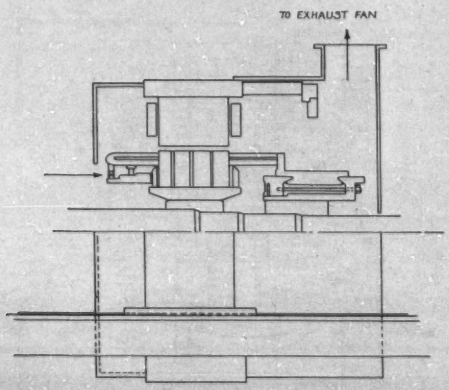


Figure 10. Cross section of a salient pole a-c generator with recirculating air cooling system and built-in cooler

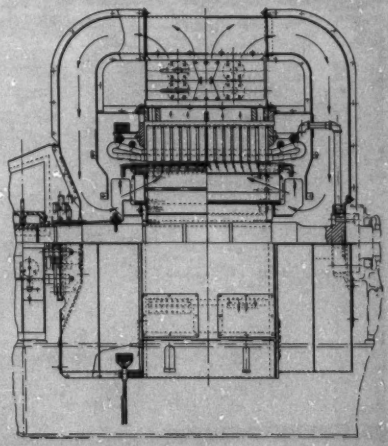


Figure 11 (left). Salient pole a-c generator with recirculating air cooling system and built-in cooler

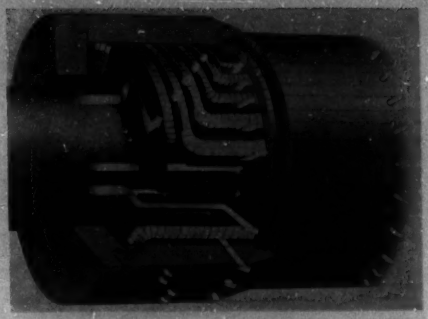


Figure 13. Air paths through rotor of a typical a-c turbine driven generator

and the air in the enclosed systems will remain free of oil vapor. The amount of air entering through the filter is only the leakage and so the electrostatic filter can be very small. The pressure drop through this point in the system is essentially atmospheric. If it is necessary to have a duct of any length so some pressure drop is introduced at the air entrance, a small blower may be used to overcome this drop and "pump up" the system slightly so all air leakage is outward.

Electric machinery on shipboard is practically always installed in a more or less confined space. This means that forced ventilation is necessary in order to remove the losses from the machinery space even when it might not be necessary to force ventilate the machine for any other reason. If the hot air were not removed, the ambient temperature would rise to prohibitive values and even if insulation were used which could stand abnormal temperatures, it would be unsatisfactory from the standpoint of operating personnel. In such cases the so-called induced draft system of ventilation may be used. Figure 8 illustrates this type of ventilation schematically. The air enters at the rear through an ordinary antirecirculation type of end bell and a connection to an exhaust fan is made by means of a chimney at the top of a totally enclosing front end bell. An actual shipboard installation of this type is shown in Figure 9. Two machines have a common exhaust duct which draws the hot air out of the front end of the machines and discharges it above deck.

The illustrations used so far have all referred to d-c machines. The same fundamental principles apply, of course, to a-c machines. There are two main differences, and these differences modify the flow of the ventilating air. First, the stator of the a-c machines is laminated and this allows the air from the ventilating ducts to be discharged radially. This, of course, is not possible with the solid yoke of the d-c machine so that air must flow axially from rear to front. Second, the

Figure 12. Self cooled recirculating air system of a nonsalient pole a-c generator

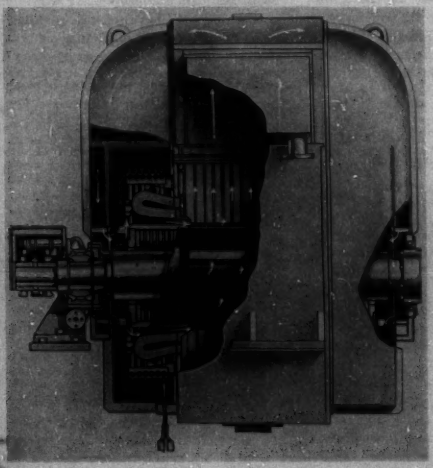
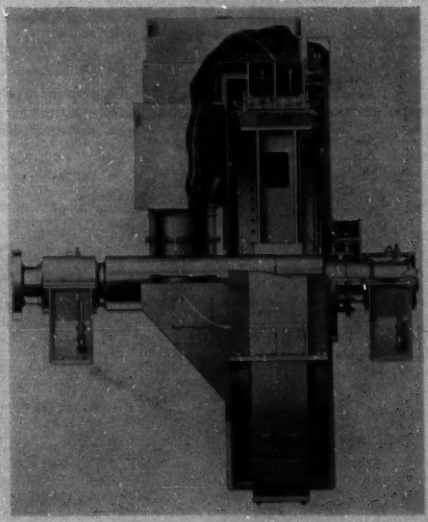


Figure 14. Ventilation system for a large a-c propulsion motor with propeller type fan



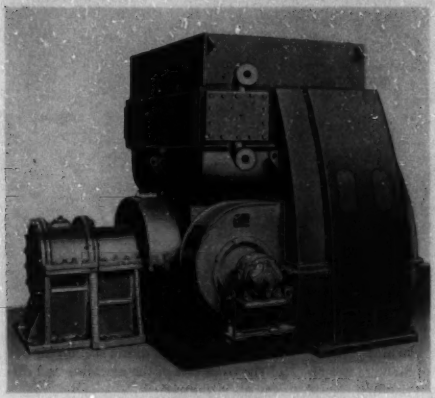


Figure 15. Ventilation system for a large a-c propulsion motor with centrifugal-type blowers

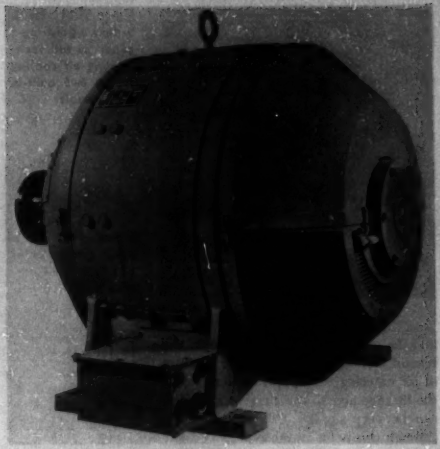


Figure 17. D-c drip-proof, semienclosed pump motor

a-c machine, having no commutator, is symmetrical so air can flow from both ends and be discharged radially.

Figure 10 is a cross section of a salient pole a-c generator self-cooled with a recirculating air system and built-in cooler. The paths of the ventilating air are indicated by the arrows. The blower blades at each end of the rotor, as well as the rotor itself, force air into the air gap. It is then discharged radially through the stator vent ducts. Solid frame covers collect the air and direct it into the bottom of the cooler. The cool air is discharged from the top of the cooler, where half of it flows to the front and half to the rear, through suitable ducts directing the air back into the end bell and blower entrances. An illustration of such a machine, with all enclosures in place, is shown in Figure 11. A very similar type of ventilating scheme, except for a nonsalient pole a-c generator is indicated by Figure 12. The rotor of a machine of this type is rotating so fast that it is extremely effective in cooling itself providing suitable paths for air flow are arranged. Figure 13 shows the paths of air through the rotor of a typical turbine-driven generator.

A large a-c propulsion motor is illustrated in Figure 14 with the path of the ventilating air indicated by the arrows. At the left can be seen the built-in cooler and propeller-type blowers. A similar type of ventilating scheme for a propulsion motor is shown in Figure 15 except centrifugal-type blowers are used instead of propeller type.

Auxiliary equipment on shipboard is often located in such a manner that serious limitations are imposed on ventilation. Motors must have enclosures varying from drip-proof and splashproof to totally enclosed. Auxiliary motors must sometimes be placed in locations where they are subjected to very oily atmospheric conditions so that nothing short of totally enclosing the motor will prevent undue maintenance. Obviously it would be impractical to have separate ventilating systems for small auxiliaries such as

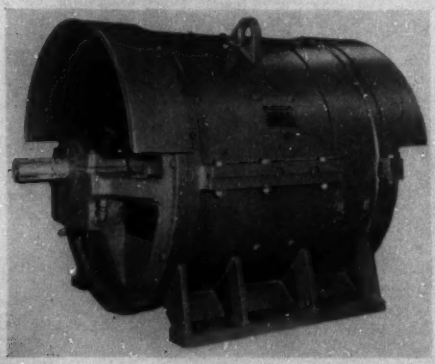


Figure 16. D-c compressor motor with drip-proof covers

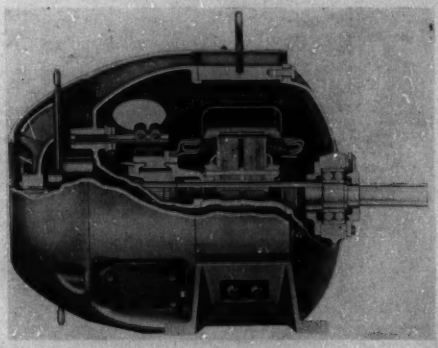


Figure 18. Totally enclosed fan cooled motor

have been described here for propulsion equipment and larger auxiliaries. In small sizes these auxiliary motors are under rated and built totally enclosed without any separate ventilation, depending only on the external exposed surface to dissipate the losses. There is a very definite limitation on the size of the motor which can be built in this manner. This is because the rating and losses vary approximately with the square of the diameter whereas the surface increases only as the first power of the diameter. Increased output can be obtained by adding external fins thus increasing the radiating surface. The limit can be extended still farther by the use of the so-called fan-cooled type of motor. This motor is designed with fins on the inside surfaces and an internal fan to give the best possible heat transfer from the active material to the heat conducting surfaces. An external shaft mounted fan blows air over the external fins instead of depending only on the natural radiation.

It is in this field, where machines have a very high degree of enclosure, that the use of silicone insulation will prove to be of maximum value. This is because the temperature limitation is reached long before any such limitation as efficiency, pull-out torque, commutation, and so forth, is reached.

Figures 16, 17, and 18 show auxiliary marine equipment with varying degrees of enclosure. Figure 16 is a compressor motor with drip-proof covers. Figure 17 is a drip-proof semienclosed pump motor. Figure 18 illustrates a totally enclosed fan-cooled motor.

In conclusion it may be stated that installations of electric equipment on shipboard make it necessary to provide machines with varying degrees of enclosures. The location of the machinery imposes also certain restrictions on the ventilation system. Due to the temperature limitations of insulation, these machines must be cooled or ventilated and it is the problem of the designer and application engineer to do this with the most economical use of space and material. In some cases it may simply mean supplying machines with the correct enclosure to protect the machine properly. Specifications should never call for a degree of enclosure more than is absolutely necessary, as any restriction in ventilation will reduce the output obtainable from a given machine.

Reference

1. Surface Heat Transfer in Electric Machines With Forced Air Flow, G. E. Luke. AIEE Transactions, volume XLV, 1926, pages 1036–47.

Discussion

W. H. Fifer (Navy Department, Washington, D. C.): Proper ventilation of rotating electric equipment on shipboard not only depends on proper design of the equipment itself but is also equally dependent on proper installation. Installation features requiring close attention on naval vessels are as follows:

1. Naval vessels must be capable of operating in the tropics under darkened ship conditions which greatly increases the ventilating problems. Machinery space ventilating systems must be designed which are capable of maintaining an ambient temperature which is within the maximum ambient temperature for which the electric equipment is designed.

2. When open rotating electric machinery is used, the ventilation problem is greatly aggravated due to the large amount of heat given out by the machine. For example, an installation composed of two 1,000-kw generators supplying power to two propulsion motors of equal size would dissipate a total of 240 kw, assuming the efficiency of the motors and generators to be 94 per cent. When this heat is added to the heat losses of the Diesel engines and other equipment in the machinery spaces, it becomes a very difficult problem to supply sufficient air to maintain the ambient within practicable limits.

3. The ventilation problems for machinery spaces of Diesel-driven vessels is further aggravated by the requirement for engine intake air. Since most of the engine manufacturers object to using hot air for the engine induction, the engines require a large amount of the cool air from the machinery space ventilation inlets. This greatly increases the total amount of inlet air required.

4. Another installation problem requiring close attention with open machines is the recirculation of the air within the machinery spaces. For example, electric machinery located adjacent to other equipment may draw its inlet air from the hot discharge of the adjacent equipment. Also, baffles are frequently required to be placed under the foundations of open electric equipment to prevent sucking its own hot discharge air back into its inlet.

5. Another problem which has given considerable difficulty on naval vessels is the distance from the bilges which must be maintained to prevent water from splashing into or entering open machines. On naval vessels, where open machines must be located close to the bilges, the lower half of the machines are enclosed in watertight casings which makes the ventilation problems within the machine more difficult.

6. It is noted from Figure 9 of Harrison's paper that some installations are made with suction-type ventilation attached directly to the machines. It has been found that this method is not practicable for use on naval vessels, since either extremely adverse conditions of weather or untrained personnel forcing water into the topside inlets when washing down the decks with hoses has invariably resulted in salt water finding its way into the electric machinery.

7. Experience on naval vessels has indicated that use of the double-tube-type of air coolers is the only practicable way of keeping salt water from getting into electric equipment fitted with air coolers. With the higher temperatures being used with present-day machines and insulating materials, a leak from a single tube cooler will almost immediately vaporize and carry salt through the entire system.

8. As noted from Figure 13 of Harrison's paper, a considerable amount of air can be made to flow through the cylindrical rotor of an a-c turbine generator. Unless a recirculating system using an air cooler is employed with this method, oil vapor from the machinery space will soon contaminate the windings of the rotor.

Experience obtained from the construction and operation of over 1,000 electrically propelled vessels during the past war has definitely indicated that the only satisfactory method for ventilating large rotating electrical machinery is by the use of the recirculating systems employing double-tube air coolers.

The additional initial cost of this type of equipment is far more than off-set by the reduction in maintenance of the equipment and the additional cost of supplying adequate machinery space ventilating systems for use with open machines.

J. F. Sellers (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper by Harrison gives a very good description of the experience gained in the large-scale application of d-c machines on shipboard during the war. Except for auxiliary d-c motors, most of the applications did not involve severe commutating problems because of the overloading limitations of the prime movers, such as Diesel engines and turbines. The use of silicone insulation, especially in larger machines, had not progressed to the point where commutation and mechanical problems became the limitations. Thus, the problem on d-c machines was to concentrate on ventilation methods to obtain the greatest output from each machine, consistent with good insulation, brush, and commutator reliability.

The use of either axial air ducts or radial ducts can be decided by direct comparison with the same armature size, and it also may be decided by the application. Axial ducts are used usually where blowers are available, and where coil insulation must be protected from large amounts of dirt, especially abrasive dirt.

Reduction of the bad effects of carbon dust on insulation resistance by the flow of cooling air from the back of the machine to the commutator end was generally recognized, even though it resulted in somewhat

higher operating temperature for the commutator and brushes. For 40-degree centigrade rise machines the temperature effect on the brushes was not noticeable, but, for 70-degree centrigrade rise and silicone machines, the increasing commutation and brush problems with each higher ten degrees (centigrade) came to the front. This situation has led to the study of a ventilation system which will keep the carbon dust out of the machine windings, and still give the coolest air possible to the commutator and brushes. It is our opinion that the full use of silicone insulation and such a ventilating system in the future will give such an increased output per pound of active material (in machines of larger size) that considerable research and improvement will be needed in commutation adjustment and the mechanical design of commutator, brush, and brush holders.

Shipboard Degaussing Installations for Protection Against Magnetic Mines

NICHOLAS B. MICHEL
NONMEMBER AIEE

EARLY in World War II the German navy and air force started to lay a new type of sea mine, reputed later to be the first of the "secret weapons." This mine was detonated at a distance beneath a ship by the ship's influence rather than by actual contact as was necessary for previous types of mines. Due to the mystery surrounding the means of operation of this mine and the lack of countermeasures, it was a threat to Allied shipping.

This was the situation when one night in November 1939 German planes were observed to release strange ground mines with parachute attachments which fell into shallow water off the English coast Several of these mines were recovered by British mine officers and successfully dismantled. Upon removal of the mine dome, a manual latitude adjustment was revealed labelled "Gauss" which immediately confirmed suspicions that the new weapon was a magnetically-operated mine.

Further examination proved this correct since the sensitive firing mechanism included as the actuating element a magnetized needle mounted so as to complete an electric circuit upon a predetermined swing of the needle. This initiated detonation of the mine. It was found that the distortion caused in the earth's field by the passage of a steel ship was being used to upset the balance of a magnetized needle in the mine causing it to detonate beneath the ship.

As a result of the use of this mine the United States Navy set forth on an extensive program of reducing the magnetic field of its ships by the use of demagnetizing or "degaussing" coils. This program involved fitting approximately 12,000 naval and merchant ships with coil installations, which along with the minesweeping program were successful during the war in affording such protection that the losses from this type of mine suffered by our ships were small.

The Problem and Solution

The problem of protection of ships against magnetic mines offered only one solution, other than that of sweeping the mines, and that was to reduce the magnetic effect of the ship on the mine so as not to swing the mine magnetic needle to the contacts. This process became known as "degaussing."

Degaussing took two main forms. The first of these consists of temporarily wrapping a coil of heavy cables around the outside of the ship, either in a horizontal plane encircling the hull at about the waterline, or in solenoid fashion about the centerline of the vessel with cable loops in vertical planes encircling the vessel from weather deck and superstructure to keel with the loops perpendicular to the centerline. Controlled direct current in definite schedules of pulses passed through the temporary cables produces strong magnetic fields which reduce the original magnetic field of the vessel. This type of degaussing is known as "magnetic treatment" and is temporary in nature since after the coils are removed and the ship operates in the earth's field, the effect of the treatment decays. This treatment is done by especially equipped "degaussing stations."

The second type of degaussing consists of installing permanent coils on the ship which when energized with direct current set up a magnetic field that opposes the ship's magnetic field and thereby reduces the change in the earth's field beneath the ship. This type is known as a "shipboard degaussing installation" and is the main subject discussed by this paper.

The Earth's Field

The magnetic field of the earth can be represented as shown by Figure 1 with flux lines leaving the earth's magnetic South Pole and entering at the magnetic North Pole. The field surrounds the earth at all points and for small regions can be assumed to be of uniform strength. If the field is considered at some point A on the surface of the earth, the field can be imagined to exist as shown by Figure 2. If a steel ship now steams by point A, the distortion shown by Figure 3 results. Since a magnetized needle in a magnetic mine tends to line up with the lines of flux, it is evident that rotation of the needle would result as the ship steams over the mine, momentarily bending the lines of flux from their normal positions.

For purposes of degaussing it is convenient to resolve the earth's field into its vertical and horizontal components (Figure 4) and to treat the effects of each component on the ship separately.

From consideration of Figures 1 and 4 it can be observed that the earth's horizontal component is always from south to north while the vertical component is upward in the southern hemisphere and downward in the northern. Further, the strength in milligauss of the two components varies with the latitude. The horizontal component reaches a maximum of 410 milligauss at the magnetic equator and becomes zero at the poles, while the vertical component reaches a maximum of over 650 milligauss at the poles and is zero at the magnetic equator.

The Ship's Field

The distortion of the earth's field caused by the presence of the ship in

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NICHOLAS B. MICHEL is with the Bureau of Ships, Navy Department, Washington, D. C.

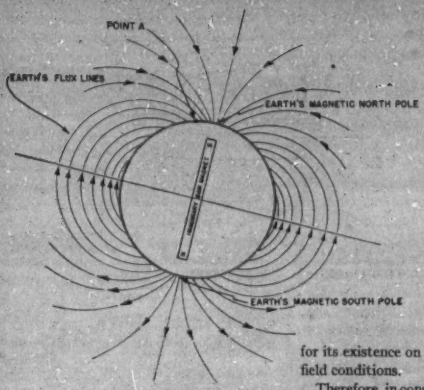

Figure 1. The magnetic field of the earth

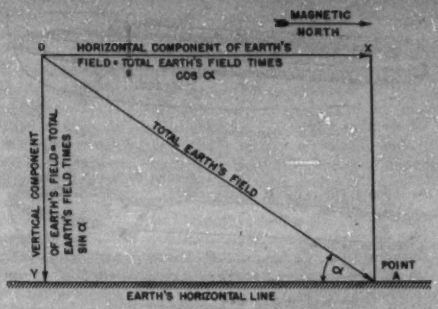

Figure 4. Vertical and horizontal components of the earth's field

Figure 3 can best be analyzed by subtracting the uniform field of the earth from the distorted field and thus obtain the field of the ship. The field of the ship can then be considered the disturbing field and conveniently resolved into horizontal and vertical components. Since the horizontal component may have any angle with respect to the centerline of the ship because of the ship's ability to change heading, it is found desirable to resolve this component further into a longitudinal component along the direction of the vessel's centerline and an athwartship component perpendicular to the centerline. The ship's field then consists of three mutually perpendicular components: vertical, longitudinal, and athwartship.

Each of these components is due to two types of magnetism, so they must be further broken down. The first type is the induced magnetism which accounts for most of the ship's field. This type is the magnetism induced in the steel and retained only while the ship is in the presence of the earth's inducing field. The second type is the permanent magnetism which is taken on by the ship's steel while being mechanically worked in the presence of the earth's field and which is retained until decayed and not dependent for its existence on the immediate earth's field conditions.

Therefore, in considering all the possible components of the ship's field which might add up to give enough magnetism to detonate the mine we would list for completeness:

Vertical	Horizontal
Induced	Induced longitudinal
Permanent	Permanent longitudinal
	Induced athwartship
	Permanent athwartship

The polarity of the vertical induced will be upward in the southern magnetic hemisphere and downward in the northern. The direction of the horizontal induced will remain from magnetic south to north regardless of the heading of the vessel. The polarities of the permanent longitudinal and permanent athwartship magnetism will be fixed over long periods with regard to direction on the ship and will be independent of heading.

The magnitude of the induced vertical component will vary directly as the variation of the earth's inducing vertical field component from a maximum at the magnetic poles to zero at the magnetic equator. The magnitudes of the induced longitudinal and athwartship magnetisms will be a function of the ship's magnetic heading as well as the strength of the earth's horizontal inducing field component.

Therefore the ship's field is a complex affair which depends on the following conditions: past magnetic history; immediate magnetic heading; and immediate latitude position.

The Degaussing Coil System

In order to obtain a degaussing coil system having its runs parallel to decks and bulkheads and permitting simple control of currents for latitude and heading changes, the degaussing coils are each designed to neutralize either specific vertical or horizontal effects so that the complete coil system would take care of all effects.

The degaussing coils and the effects they neutralize are as follows:

M-Coil. This coil encircles the ship just inside the skin of the hull in a horizontal plane approximately at the water line. This coil is shown by Figure 5. The M-coil neutralizes the sum of the induced vertical and permanent vertical magnetizations by superimposing on the ship's field a coil field having approximately an equal and opposite effect beneath the ship.

F- and Q-Coils. These coils are located at opposite ends of the vessel in horizontal planes and connected in series. The F-coil encircles the forward one-third or one-fourth of the ship and is located just beneath the forecastle or uppermost deck. The Q-coil encircles the after one-third or one-fourth of the ship and is located just beneath the quarter deck or uppermost deck. These coils are shown by Figure 6. The F- and Q-coils are connected in series with each other so as to have polarities in

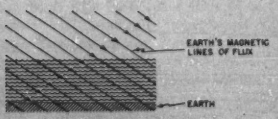

Figure 2. The magnetic field at the surface of the earth

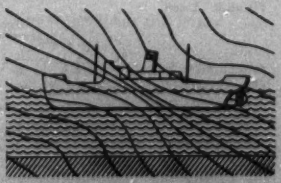

Figure 3. Distortion of the earth's field caused by presence of a ship

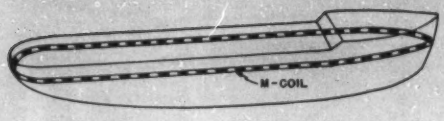

Figure 5. M-coil

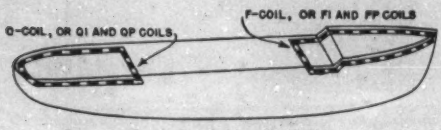

Figure 6. F- and Q-coils

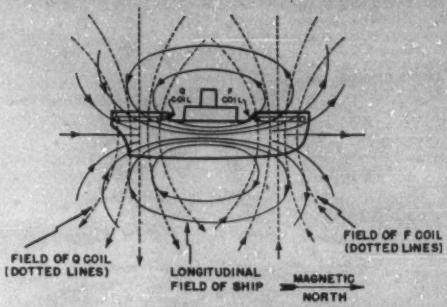

Figure 7. Neutralization of the ship's longitudinal magnetism by the use of F- and Q-coils

opposite directions. The F- and Q-coils develop strong fields below the bow and stern of the ship to approximately neutralize the sum of the induced and permanent longitudinal magnetism in these regions as shown by Figure 7.

L-Coil. In cases where a better matching of the ship's field is required than is afforded by the F- and Q-coils, an L-coil is installed in lieu of the F- and Q-coils. However, due to the increased difficulty of installing an L-coil, its installation is uncommon. The L-coil is a solenoid with its axis parallel to the axis of the ship's field being neutralized by it, thereby obtaining a better match. The L-coil is composed of a series of loops in vertical planes perpendicular to the centerline of the ship. The loops each run inside of the hull from the keel to the underside of the weather deck as shown by Figure 8. The loops are all connected in series to form the L-coil.

A-Coil. This coil runs with loops vertical in fore and aft planes running from the inside of the ship's inner bottom to the underside of the weather deck as shown by Figure 9. This coil usually consists of two loops. One loop runs the full length of the ship, while the second loop runs in the central portion of the vessel only to give added magnetic strength in this section. In the central portion of the vessel the loops are located outboard of the centerline, while the portions of the loop fore and aft of this are on the centerline. The A-coil neutralizes the sum of the induced and permanent athwartship magnetisms.

The Degaussing Cable Installation

The first degaussing installations made were in desperate haste and utilized the equipment immediately on hand at the shipyards. It was not fully appreciated that degaussing coils placed within the hull could effectively give protection. Therefore, the first installations were made with the cable clamped outside of the hull, encircling the ship at levels varying from about ten feet above the waterline to the weather deck level. Other early installations were made in the waterways of the weather deck just within the bulkwarks. At this time M-, F-, and Q-coils only were used. The cables for these installations were made in the yard by bundling together conductors of American Wire gauge number 2 rubber covered wire, tightly wrapping with canvas tape and sewing a tightly fitting canvas cover over the entire length of cable. Control of the ampere turn strength of these coils was obtained by switching turns in and out of the circuit, the coil being fed from the ship's 120-volt d-c ship's service bus.

It was quickly evident that this type of installation was not practical since the terrific force of the sea on the cables, which were up to about five inches in diameter and racked three and four cables together, tore them loose in spite of heavy straps and steel plate guards. Also, if this did not occur within a few voyages, the cables became water soaked and the conductors grounded out. The switching method of control was not satisfactory because of the coarseness of the steps.

However, in the meantime, electrical cable, rheostat, and motor generator manufacturers were rushing to completion more suitable equipment ordered by the Navy. This later equipment did prove satisfactory and was used throughout the war in the degaussing of some 12,000 naval and merchant vessels.

Based on the experience of the early installations, the subsequent ones, with few exceptions, were placed inside of the vessels. Due to the shielding effect of the steel hull and reduction of core effect, the magnetic efficiency was reduced by approximately 30 per cent, but the reduction in maintenance on the coils more than justified this move.

The prefabricated cables were of two general types for naval vessels, single and multiconductor. The single conductor cables were designed for use with low-voltage motor-generator sets on large combatant ships and the multiconductor for use in series with rheostats and supplied from the 120 or 240 d-c ship's service on the smaller combatant vessels and auxiliaries. For the merchant marine installations a larger multiconductor cable was similarly used with rheostats and supplied from the ship's service of 240-volt direct current.

Figure 8. L-coil

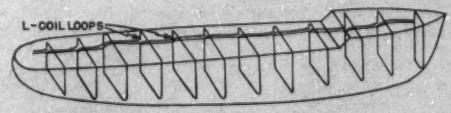

Figure 9. A-coil

The type of cable construction used on the naval installations followed the pattern of the standard Navy heat- and flame-resistant cable except that the insulation thickness was reduced in order to save critical material. This reduction was permissible due to the lower operating voltages of the degaussing coils. The most common type of single conductor cable was used in conductor sizes ranging from 400,000 to 2,000,000 circular mil areas. The conductor was insulated with felted asbestos and varnished cambric tapes, covered with an impervious nonmetallic sheath and over this a braided metal armor.

The multiconductor cable was used as a 19-conductor cable with conductor sizes ranging from 6,000 to 40,000 circular mil area. Each conductor was insulated with felted asbestos and varnished cambric tapes and an identifying braid. The assembly was held by a binder and covered with an impervious nonmetallic sheath and braided metal armor.

The outside diameter of both these types ranged from $1^1/_4$ to $2^1/_4$ inches. When it is considered that on some installations the number of these cables in the wire ways ran as high as eight to ten cables, an idea may be had of the large size of the cable runs.

On many ships the strength of portions of the main degaussing loops are changed by short loops at the level of the main loop which are connected in series with the main loop. The function of these short loops is to take care of peaks in the ship's field.

On most ships the cable runs are made on the inboard edge of the frames or through lightening holes in the frames. When a tank occurs in the way of a run that would necessitate a large deviation to jog around, the tank is pierced with a conduit for each cable. Similarly cables are run through refrigerated spaces when necessary in order to avoid large deviations.

In running degaussing cable aboard ship one unfortunate requirement that continually causes despair at the installing activities and sometimes creates bad maintenance conditions is that the location of the cable runs must be closely dictated by the magnetic design of the coil rather than by selection for ease of installation. As a result the cables many times must run through crowded or wet compartments in which relocating of equipment, piercing of closely spaced bulkheads, making of many small deviations, correction of interference with piping and so on, must be accomplished to permit the run to be made, while nearby a completely clear passageway may exist but just beyond the limits of allowable deviation for the run.

Many runs have to be made in water and oil-filled bilges. For these runs experience has shown lead covered cable to be the best solution. Earlier installations in bilges using watertight conduit were a source of trouble due to leaks developing in the conduit and eventually causing the cable to become grounded.

One type of vessel on which a complete internal degaussing installation was not found advisable was tankers. On these vessels the cargo tanks were too extensive to permit the running of cables inside the hull in conduit both from the structural and fire hazard standpoints. The cable run on these ships was made at the level of the weather deck in order to get over the cargo tanks. In the forward and after ends and at midship where deck houses cover the weather deck, the run was made inside of the deck houses just above the deck. The remainder of the run was made on the weather deck using a special cable mounted on saddles several inches off the deck and just inside of the bulkwark. Heavy steel plate guards were mounted over those portions of the run exposed to mechanical damage. These guards were designed to present sloping surfaces to the wash of the sea to prevent their being torn loose. The first type of cable used for the exposed portions of the tanker installations consisted of a multiconductor lead sheathed cable covered with jute, heavy wire armor and jute outer covering. This type cable installation was successful for about nine months after installation when the cable on numerous tankers started to ground out. Investigation revealed minute transverse cracks along the lead-sheath due to fatigue of the lead. This fatigue was due to the continual stressing of the sheath caused by vibration of the hull.

As a result of these failures an improved type of cable was developed for this service covered with a nonmetallic sheath, duck tape, and heavy steel tape armor. In this cable the resiliency of the sheath has corrected the previous trouble.

Control Equipment and Power Supplies

In installations using multiconductor cables, the coils are formed by connecting the conductors of the cables in series to obtain the desired number of turns. On the basis of the number of turns available, operating currents for the various magnetic latitude zones and heading are specified by degaussing stations where the ship's field is measured by sailing over a row of submerged magnetic detectors known as a degaussing range. The required currents are set by the ship's force by means of manual or motor operated rheostats. Figure 10 illustrates the control system of a typical degaussing installation of M-, F-, and Q-coils in which the F- and Q-coils are composed of multiconductor cable and the M-coil of single conductor cable. An A-coil was not used here because the degree of protection desired did not require it; however, the controls would have been similar to the FI-QI coil. In this figure a common arrangement of splitting the F and Q coils into separate FI-QI and FP-QP circuits is shown. The FI-QI circuit neutralizes induced longitudinal magnetism only while the FP-QP circuit takes care of permanent longitudinal. Polarity of the currents in the FI-QI and FP-QP coils is adjusted by means of the reversing switches.

In the single conductor M-coil, power is supplied from a low-voltage motor-generator set. Output current and polarity of the generator are adjusted by means of a motor operated rheostat wired to permit reversing the generator field current. In many installations the motors for the motor-generator sets are supplied from the ship's service 440-volt a-c bus instead of as shown by Figure 10 which applies to a d-c powered ship. On smaller ships the coils are all composed of multiconductor cable and controlled by rheostats. On larger ships all coils are single conductor or the M-coil single conductor and the other coils multiconductor as shown by Figure 10. The choice of single conductor cable instead of multiconductor is made when the length and ampere turn capacity are so great that the number of multiconductor cables required would be excessive.

Connections to control equipment and between cable sections are made in watertight connection boxes on terminal strips with solderless-type connectors. These connection boxes facilitate adjustment of turns by degaussing stations in order to obtain the best degaussing and permit isolation of grounded conductors. However, some trouble has been experienced due to flooding and condensation in these boxes. The solution to this problem has been in filling of the boxes with a removable polymerizing plastic compound which completely seals the box against moisture. This plastic can be removed if necessary by digging out with hand tools.

Power Requirements

Different classes of ships receive different degrees of degaussing protection de-

pending on their undegaussed field and expected duties. This means that wooden hull and small vessels would receive an M-coil only, larger vessels M, F and Q and best degaussed vessels M, F, Q, and A. The degree of protection established as the desirable standard for each type of vessel must take into account the practical aspects of the installation that would be required to meet this standard. It is necessary to follow this policy rather than to attempt to obtain the best possible degaussing for all vessels since the degree of protection obtained for the amount of effort and material expended follows a law of diminishing returns.

Perfection in degaussing by eliminating all of the ship's field is not attempted on any ship due to the physical impracticability of such an installation. However, the mine target area beneath the ships is reduced to such an extent that when coordinated with the minesweeping program, reasonable safety is obtained.

Based on these considerations the power requirements on typical representative classes of ships are listed in Table I.

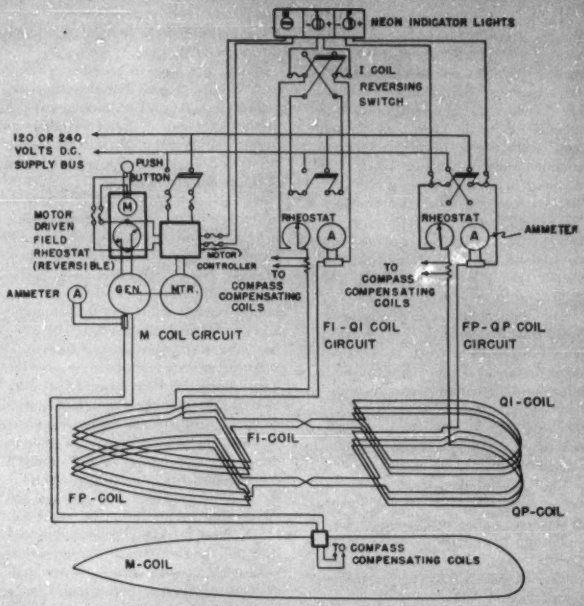

Figure 10. Typical wiring diagram for degaussing installation with M-, F-, and Q-coils using both single and multiconductor cable

Effect on Other Equipment

Since the degaussing coils must produce fields extending rather long distances, the strength of the coil fields in the immediate vicinity of the cables is sufficiently high under some conditions to adversely affect operation of other equipment. For example, this magnetic field will be about two gauss in a compartment at a distance of five feet from a degaussing coil producing 1,500 ampere turns. Closer to the degaussing cable much stronger fields will be encountered, while throughout the vessel at distances ten or more feet from the degaussing cable, magnetic fields of about one-half to one gauss may be expected. These fields will add vectorally to the earth's field in the vessel, which in navigable waters would be up to about 0.6 gauss, to produce a resultant field which may affect electric welding operations, magnetic and gyro compasses, electrical measuring instruments, radar cathode ray tubes and other similar equipment unless preventive measures are taken. The preventive measures take three forms: relocation of affected equipment away from degaussing coils; installation of shields on affected equipment; installation of compensating coils fed from the degaussing coil circuit.

Of these effects the most common is the deviation effect that would be caused on the magnetic compass by the degaussing coil fields. In order to eliminate this deviation effect, prefabricated compass compensating coil assemblies are mounted on the binnacles and fed from a voltage tapped off of the disturbing degaussing coil circuit. By means of adjusting resistors, the current in the compass compensating coils is set to neutralize the effect of the degaussing coil on the compass needles. Once this adjustment is made, the compensating current will vary as the degaussing current and automatically produce correct compensation for all degaussing currents.

Degaussing coils may interfere with electric arc welding if the field at the arc approaches a strength of about 70 gauss. Under this condition noticeable irregularities in the welding operation will occur. Steel-backing plates rigged across the joint being welded will sometimes correct this effect by reducing the field in the event the degaussing coils cannot safely be secured.

In other cases of interference, local shields on the equipment have proved satisfactory in the event that neither the equipment nor the degaussing coil could be relocated.

Table I

	Kilowatt Demand Load	Feet of Cable Used
Battleship	100	12,500
Aircraft carrier	75	15,000
Cruiser	75	9,000
Destroyer	15	2,500
Patrol vessel	3	900
Merchant vessel (Liberty Ship)	20	2,200

Conclusions

Degaussing for protection of ships against magnetic mines has proved to be practical both from the standpoint of installation and operation. While it only gives a partial and negative type of protection, its value can be realized by the rate of casualties which occurred at the beginning of the War when the magnetic mine was introduced by the Germans. Had the enemy withheld the use of the magnetic mine until it could have been first laid in very large quantities, it would have been highly successful due to the lack of available countermeasures. As it was, the Germans laid about 8,000 magnetic mines the first year and increased

the rate to 12,000 or 18,000 in the second year.

By the third year the rate decreased sharply due to the diverting of most of the aircraft groups, which laid the majority of the mines, to the Russian campaign. Also, the early mines were of a 50 milligauss type which was a comparatively low sensitivity. Protection by degaussing against this low-sensitivity mine was easier to obtain than if a high-sensitivity mine had been used since the fitting of the coils was not as critical. This fact aided the cause of degaussing by permitting it to get established before the requirements were tightened up by the use of more and more sensitive mines. The enemy was forced into the use of these more sensitive mines by the fact that degaussing reduced the possibility of the insensitive mines being detonated by the degaussed ships. This development, however, has an advantage in the fact that the more sensitive mines may be swept over larger areas and depths at every pass by the minesweepers.

At the present time the degaussing installations on all naval vessels are being maintained and have become a permanent part of the vessels' electric equipment. In some parts of the world these vessels are still required to operate their degaussing installations due to the existence of magnetic mines which have remained unswept and which may still be dangerous to navigation.

Discussion

A. Solem (Navy Department, Washington, D. C.): For an effective degaussing program, the ships' degaussing coils must be used properly and the ships must be in standardized magnetic states which these coil systems can degauss. After degaussing coils have been installed, each ship visits a degaussing station to be degaussed to the limits required by the general program. At the degaussing station, the ship's magnetic fields are measured; if necessary, the ship is depermed; and the ship's degaussing coils are calibrated. Deperming is the magnetic treatment which alters the ship's magnetic state so that its degaussing coils will satisfactorily degauss the ship. Degaussing calibration consists of determining and transmitting to the ship the proper degaussing coil currents needed to minimize the ship's external fields. Further degaussing action for the ship, aside from using the coils as instructed, is an occasional checking of the ship's degaussed condition and new depermings and calibrations as is found necessary.

Degaussing Stations

Degaussing stations are made up of deperming facilities where depermings and calibrations are performed, and channel ranges where periodic checkings and general calibrations are performed. The size of the degaussing station varies with its location and the type of ships it handles. A large station may have several ranges and deperming facilities; a small station may consist of only a single channel range.

Deperming Facilities. The deperming facility is located, if possible, at some quiet but easily accessible spot within the harbor which the degaussing station serves. It usually consists of a magnetometer garden fixed in the harbor bottom adjacent to a pier, where the magnetometer controls and deperming power supply are located. A facility for small ships will have approximately 50 detectors spaced about 20 feet apart at a 30- to 40-foot depth, spread out to give a complete survey of fields in the area alongside the pier. A facility for large ships will have up to 75 such detectors spaced approximately 40 feet apart at a 60- to 70-foot depth in the survey area. The detectors are set in copper pipes jetted into the bottom; hence, their depth is not limited by harbor depth. Some gardens have two depths to handle all sizes of ships.

Measurements at the deperming facility are made with the ship moored directly over the detectors. The ship's signature or field consists of the magnetic-field values at a series of points spread out on a horizontal plane beneath the ship. The necessary longitudinal and athwartship profiles and the contours of the ship's over-all field can be constructed from these point values.

When the installation of an elaborate magnetometer garden is not warranted, it is sometimes possible to measure the ship's fields with portable magnetometers. With the ship at anchor, the magnetometer is suspended in turn at various points along the ship's length, and a field measurement is obtained at each point. These measurements are then used to construct the signature. The magnetometer must hang freely beneath the ship, and the harbor depth, unless it is very deep, limits the use of portable magnetometers.

Channel Ranges. The channel range is located in a ship channel where all ships may use it. The range detectors (range coils) are located in the channel bottom on a line perpendicular to the direction of ship passage through the channel. A range for small ships will have approximately 20 detectors spaced 10 to 20 feet apart at a water depth of 30 to 40 feet. A range for large ships will have approximately 30 detectors spaced 20 to 40 feet apart at a depth of 50 to 75 feet. Here also, the detectors are attached to piling or set into copper pipes jetted into the channel bottom, and their depth is not limited by channel depth. A range house overlooking the channel contains the recording instruments (fluxmeters), and serves as the control station governing movements of ships on the range.

As the ship passages over the range the fluxmeters make continuous records of the magnetic field at each detector. Each passage of the ship over the range produces a series of equally spaced longitudinal traces of the ship's magnetic field which comprise the ship's signature. The traces may be used independently or the data from them may be used to draw contours of the ship's signature.

Some less elaborate ranges use a long narrow loop stretched across the channel as the detector. The loop is about 500 feet long and 10 feet wide. It lies on the channel bottom, and its depth must be the same as the channel depth. This type of range does not give magnetic field measurements as a series of longitudinal traces, but does give the magnetic flux changes produced by the ship which can be used to check the condition of ships.

The detectors of the deperming facilities and the channel ranges are oriented to measure the vertical component of magnetic field. Undue complications would result if total field measurements were required (simultaneous measurement of three mutually perpendicular components would be needed), and the instrumentation is easier for the vertical component than for one of the horizontal components. The vertical field components are also larger than the horizontal components, and hence give clearer pictures of the degaussed condition of the ship. Whenever possible the channel ranges and deperming facilities are located such that the ship's fields are measured on north-south heading.

Deperming

Ships are depermed to standardize their magnetic states. Ideally, the postdepermed state has zero PLM and PAM, and a $PVM + IVM$ in magnetic equilibrium with the earth's vertical field.* The deperming process consists of mooring the ship at the deperming pier, wrapping it with the deperming-solenoid shaking coil, and then shaking it magnetically with a series of d-c pulses in the solenoid. These current pulses, furnished by a large number of storage batteries in a series-parallel connection, are of alternate polarity. High currents are used initially with the current values diminishing through the schedule. Thus the ship's steel is carried through a demagnetization schedule of diminishing hysteresis loops. The horizontal component of the earth's field is balanced out by a compensating coil during treatment to produce the demagnetization of LM and AM. Since the vertical component of the earth's field is present during treatment, the shaking schedule increases PVM to a maximum value so the total VM is in equilibrium.

Deperming of a ship has to be repeated whenever the vessel develops excessive PLM or PAM, or irregular VM. Some vessels require redeperming within a few

* The following technical abbreviations have been used in this discussion:

VM—vertical magnetization
LM—longitudinal magnetization
AM—athwartship magnetization

IVM—induced vertical magnetization
ILM—induced longitudinal magnetization
IAM—induced athwartship magnetization

PVM—permanent vertical magnetization
PLM—permanent longitudinal magnetization
PAM—permanent athwartship magnetization

months of a previous treatment; others may never require retreatment. When a ship will require retreatment is unpredictable, and practical scheduling of deperming is possible only with frequent checking of the degaussing of all ships.

Calibration

Procedure. Calibration of the ship's degaussing system may be performed on either the channel range or the magnetometer garden. Ships depermed are ordinarily calibrated on the magnetometer garden at completion of the deperming; all others are calibrated on the range. Calibration procedure on a north-south channel range is as follows:

1. The ship's undegaussed signature is obtained on two passes of opposite headings across the range.

2. The M-coil is energized and the current in it adjusted on successive runs across the range until the residual field under the middle third of the ship is reduced to a minimum. The M-coil compensates *VM*, and its current will be the same on both headings.

3. With the M-coil set at best current the FI-QI or L-coil is energized and the current in it adjusted such that a given positive current on north heading and a negative current of the same magnitude on south heading produce uncompensated residuals of the same shape and magnitude on both north and south headings. The FI-QI or L-coil compensates *ILM*. Its current has the same magnitude on north and south headings but opposite polarities.

4. With the M and FI-QI coil currents adjusted to proper values the FP-QP coil is adjusted to reduce the remaining misfits at bow and stern. This may be an adjustment of both currents and loop turns. The FP-QP coil compensates *PLM* and M-coil misfit, and its current will be the same on both headings.

5. If the ship has an A-coil, the A-coil current is adjusted to remove any athwartship asymmetry of the signature. This is *PAM* compensation. *IAM* compensation will be accomplished by measurements with portable magnetometers when the ship is moored on east-west heading.

Ordinarily a calibration does not require all the steps indicated since only one or so coils require changing. Statistical values of the FI-QI and A currents are used also to shorten the procedure. Often a calibration can be effected in one or two runs.

Calibration procedure on the magnetic garden is similar except that all possible adjustments of currents are made while the ship is on one heading; the ship is then swung to the opposite heading and the current values are checked for validity.

Degaussing Charts. The degaussing current settings are transmitted to the ship on degaussing current setting charts. Each coil is given a set of currents valid for worldwide operation. The M-coil current varies with variation of the earth's vertical field, but not in direct proportion because of the presence of *P VM*. M-coil currents are positive in the northern hemisphere, tending toward zero at the equator, and becoming negative in the southern hemisphere. The FI-QI- or L- and A-coil currents vary directly with the earth's horizontal field. They are maximum at the equator and decrease both north and south of the equator. In addition the FI-QI current varies as the cosine, and the A current varies as the sine of the heading angle. The FP-QP varies with location if it compensates M-coil misfit, and it is constant if it compensates *PLM*. The calibration determines the setting constant for the M- and FP-QP coils, and the proportionality constant for the FI-QI and A-coils. The variation of the M-coil current with location is determined statistically from data on many ships.

Changing of current with change of heading or location is not continuous, but occurs in steps. This relieves the ship of keeping a continuous degaussing watch, and permits degaussing duties to be combined with some other duty.

Check Ranging. Check ranging requires that the ship pass over the range having its degaussing coils energized with the current settings given at time of last calibration.

At completion of the check the ship is informed of its condition and corrective action needed, if any. The ship then makes arrangements with the degaussing station to complete the action.

E. Peizer (Navy Department, Washington, D. C.): I wish to outline some of the main features of the instruments used in the methods of measurement of ships' fields outlined in the previous discussion. These methods were: coil ranges, loop ranges, fixed magnetometer proving grounds, and portable magnetometers.

Coil Ranges

For coil ranges the basic equipment consists of fluxmeters with associated control gear in the range house electrically connected to a line of detector coils at the sea bottom. Measurements of a ship's field are recorded by a pen on a strip-chart in the fluxmeter when the ship passes over the line of coils.

The principle of measurement of magnetic field by a fluxmeter-detector coil combination is as follows:

1. A fluxmeter is essentially a d'Arsonval-type moving-coil galvanometer in which the suspension restoring torque has been effectively removed or cancelled out. The moving-coil of the galvanometer is connected to a detector coil at the location where the measurement of field is desired.

2. If the magnetic flux through the detector coil changes, an electromotive force is generated producing a current through the circuit.

3. This current passing through the galvanometer moving-coil rotates the moving-coil in the field of the galvanometer pole pieces in the direction which introduces flux linkages into the circuit of opposite polarity to those introduced at the detector coil, thus tending to reduce the net flux change in the circuit.

4. If the galvanometer moving-coil is sufficiently responsive it will move at all times in such manner as to limit the net flux change in the circuit to a negligible value.

5. For a uniform field at the moving-coil, the deflection of the moving-coil from its initial position will be proportional to the change in flux through detector coil from its initial value.

6. The fluxmeter deflections are thus proportional to the change in average field intensity over the cross-sectional area of the detector coil. For the detector coils used at ranges the variation of the field from the average value is of no significance.

The fluxmeter in use by United States Navy channel ranges has a sensitivity of about 300,000 flux linkages (centimeter-gram-second units) for full scale deflection. This instrument was originally developed by the General Electric Company in 1941. A standard taut-suspension type, moving-coil galvanometer was modified to serve as a fluxmeter. The restoring torque due to the suspension was balanced out by means of a magnetized needle (magnetic dipole) mounted on the suspension and positioned between a pair of soft iron plates whose spacing was adjustable. The soft iron plates concentrated the stray field of the pole pieces of the galvanometer and produced a relatively uniform field at the dipole whose intensity could be varied by adjustment of the plate spacing. With the dipole suitably oriented, the torque on the dipole due to the field was proportional to the sine of the angle of displacement from the position of zero suspension torque and directed outward from this position. For small angles the torque on the dipole thus approximately compensated the restoring torque of the suspension. In addition to the restoring torque of the suspension which was symmetric about the zero position, an asymmetric torque was noted. This torque was largely compensated by means of a rotatable ring containing a piece of soft iron which concentrated additional field between the pair of plates.

One of the main difficulties encountered in the operation of the fluxmeters at degaussing ranges was the tendency of the meter to drift when no ships were passing, that is, when no signal was being applied at the detector coil. This drift originated primarily from thermal junction electromotive forces in the fluxmeter and from sea battery electromotive forces in the underwater installation. Manual drift control was relied upon at first. Later an automatic drift control was developed which considerably simplified the operation of the ranges.

The detector coils used in coil ranges were normally mounted with axis vertical for measurement of the change in vertical component of field. Air core coils were first used which consisted of a number of turns of multiconductor cable wound on a circular frame about five feet in diameter. These coils were replaced by coils with high permeability cores (Permalloy or Mumetal) which are now standard. Sensitivities of the order of 10,000 flux linkages (centimeter-gram-second) per milligauss were used. Introduction of the high permeability core permitted design of a much more compact coil using a large number of turns of fine wire.

These coils were generally mounted at the sea bottom either by suspension in pipes driven vertically into the sea bottom, or by gimbal mounting in an outer case which was mounted on a tripod.

Loop Ranges

Loop range equipment operates on the same general principles as that of a coil range. The main difference is that a loop of multiconductor cable about ten feet wide and several hundred feet long is used for collection of magnetic flux from the ship in place of the detector coils discussed previously. Since the loop extends over a large area the total flux linkages through it are no longer a direct measure of the field strength at any particular point and hence interpretation is not as simple.

Fixed Magnetometer Proving Grounds

Magnetometer proving grounds differ basically from the coil and loop ranges discussed previously, in that a reading of the total field (ship's field plus background) is made with the ship fixed in place. The ship's field is then obtained by subtracting the background reading.

The magnetometer used, which was developed by the General Electric Company, works on the principle that if the inductance in an a-c circuit varies differently over alternate halves of a current cycle, second harmonics are introduced in the current. The fundamental circuit consists of an exciter coil with a high permeability (Permalloy or Mumetal) core, a resistor, a d-c microammeter, and a source of a-c voltage all connected in series. The a-c voltage is such that the current flowing drives the core into saturation on both halves of the cycle.

If no external field acts on the core the inductance of the exciter coil is essentially zero for equal periods of time on both positive and negative halves of the cycle.

If an external field acts on the core of polarity so as to add to the coil field for positive current, the core remains in saturation, and the inductance is zero, for a longer period on the positive half of the cycle. As a result even harmonics are introduced in the current and its wave form shows a higher positive peak than negative peak. However, if the resistance is linear, it can be shown that no average or d-c component of current flows.

A d-c component of current is then obtained by using a nonlinear resistor with resistance which falls as the current increases. This direct current is a measure of the external field applied and is read on the microammeter.

In order to eliminate most of the alternating current through the microammeter a double circuit with a common leg containing the microammeter is used.

In the General Electric magnetometer, the detector unit contains two Permalloy cores with exciter coils wound upon them, a neutralizing solenoid enclosing the core and coil assembly, and two nonlinear resistors. The remaining components are contained in the control panel. The circuit just described is generally used only to indicate zero field; that is, act as a null indicator. Direct current is passed through the neutralizing solenoid and adjusted to such value that the microammeter in the null circuit indicates zero net field at the Permalloy cores, that is, the field due to the solenoid is equal and opposite to the external field present. The external field value is then obtained by measurement of the neutralizing solenoid current by means of a milliammeter, potentiometer or decade set with scale calibrated in milligauss.

Control panels using the foregoing basic circuits have been made for operation of up to 150 magnetometer detectors in succession. In some installations automatic recording is done by means of a modified Leeds and Northrup Speedomax strip chart recorder in conjunction with an automatic stepping switch on the control panel. On one installation an automatic recorder, designed by Bell Telephone Laboratories, typed out the field readings on a sheet of paper in the same arrangement as that of the magnetometer detectors in the proving ground array.

Portable Magnetometers

Portable magnetometers operate on the same basic principles as the fixed magnetometers used in proving grounds and no additional comment will be made.

Compensation of Effects of Degaussing Coils on Shipboard Magnetic Compasses by Use of Compass-Compensating Coils

R. A. ROBINSON
NONMEMBER AIEE

DEGAUSSING coils produce magnetic fields which counteract the external magnetism of a ship and thus afford protection against magnetic mines. These degaussing-coil fields in the ship cause the magnetic compass to deviate. Since the deviation changes as the degaussing currents are varied, permanent magnets cannot be used to compensate these errors as is possible in correcting errors in the normal adjustment of the compass. Compensation for the effects of the degaussing coils is accomplished by compass compensating coils mounted on the compass binnacle. Compass compensating coils neutralize the portion of the degaussing coil magnetic fields near a ship's magnetic compass which cause the compass to behave erratically. This is accomplished by producing compensating-coil fields at the compass which will be continuously equal and opposite to the degaussing fields at that point and will compensate for the errors due to degaussing.

Theory of Compensation

The installation of degaussing coils on all sizable vessels came as a sudden blow to navigation by magnetic compass. The previous complications resulting from variation and deviation were nothing compared with the tremendous changing deviations present when degaussing coils were energized. An initial attempt to use magnets to compensate magnetic compasses for degaussing-coil fields proved unsuccessful. Since it was desirable not to have to forego the use of the magnetic compass when the degaussing coils were energized, some means of correcting the degaussing deviation had to be devised. The use of compass-compensating coils solved this problem. This solution involved setting up a magnetic field at the compass position continually equal and opposite to the degaussing field at that point, using three mutually perpendicular coils connected to the degaussing coil through control resistors.

Figure 1 illustrates in one plane how the compass would be in error by angle θ if the earth's field, E, had superimposed on it the degaussing-coil field, D, at the compass. In this case, the compass would line up with the resultant of these fields, R, instead of with the earth's field as desired. Figures 2 and 3 illustrate two applications employing the 3-component principle of compensation. The first of these methods, illustrated by Figure 2, uses "cardinal" component coils for the horizontal effects, located as shown. The second method, illustrated by Figure 3, uses "intercardinal" component coils, displaced 45 degrees from the arrangement of Figure 2. In both cases, the vertical effects are compensated by a horizontal coil not shown.

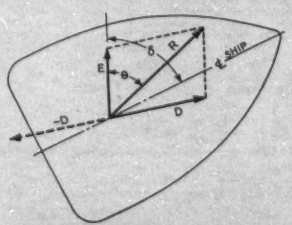

Figure 1. Action of degaussing coil field in changing directive force at magnetic compass

Vertical Effect

Almost all degaussing coils create some vertical magnetism at the compass. Vertical magnetism at the compass causes the compass to deviate when the ship heels, even though the compass may have no deviation when on an even keel. The reason for this is that on an even keel the vertical degaussing component adds to or subtracts from the vertical component of the earth's magnetic field, whereas with the ship heeled, the vertical degaussing component inclines with the ship, thus producing a horizontal component, causing deviation on the compass. To compensate for this vertical magnetism caused by the degaussing coils, a compass compensating coil, called the heeling coil, is used. The heeling coil is placed in a horizontal plane around the binnacle, usually at the level of the compass needles, with its vertical axis passing through the center of the compass. A heeling coil is used as a part of all types of compass-compensating coils.

Horizontal Effect

In addition to providing compensation for the vertical component of the degaussing field, compensation must be provided for the horizontal component. Horizontal degaussing fields at the compass cause compass deviations whether or not the ship is on an even keel. This deviation due to degaussing on any heading may be obtained by subtracting the magnetic compass reading with degaussing coils de-energized from the magnetic compass reading with degaussing coils switched on. Compensation for the

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R. A. ROBINSON is with the electrical branch of the Bureau of Ships, United States Navy Department, Washington, D. C.

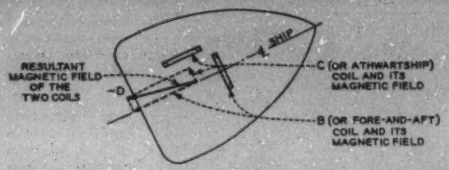


Figure 2. Compensation with cardinal components

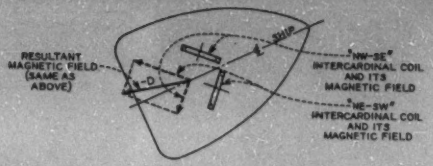


Figure 3. Compensation with intercardinal components

horizontal degaussing component is accomplished using two vertical coils, or pairs of coils, mutually placed at right angles. The magnitude and direction of compensation is changed as desired by varying the currents in each circuit.

Assuming that, in Figure 1, the arrows represent the direction and magnitude of the horizontal magnetic components of the earth's field, E, and one degaussing-coil field, D, at the compass when the ship is on a particular heading, it is necessary that the compass coils produce the component, D, which will cancel the effect of D.

Since E remains fixed with respect to the earth and D changes in direction with the ship's heading, the compass would be in error by an amount equal to the changing angle, θ, if the degaussing field, represented by the vector, D, were not counteracted by a compensating-coil field, represented by the vector, $-D$. The field, $-D$, can be produced by any two horizontal component magnetic fields at right angles, if the magnitude and polarity of each is adjustable. In compass compensation, these fields are produced by coils so placed that their effects are either fore-and-aft and athwartship (cardinal points of the compass) or displaced 45 degrees from the fore-and-aft and athwartship axes of the ship (that is in line with the intercardinal points of the compass).

It can be seen that the component, $-D$, is produced by either the cardinal set of right-angle coils shown in Figure 2 or the intercardinal coils in Figure 3. Similarly, any other desired horizontal field may be produced by a set of right-angle coils by merely adjusting the magnitude and polarity of the currents in the individual coils to produce components with the desired strength and direction. Since, in normal compass adjustment, the fore-and-aft magnetic effect is called B, this component in degaussing compensation is also called B; similarly, the athwartship component effect is called C. The intercardinal components (northwest-southeast and northeast-southwest) and the coils that produce them, are so termed because of the relative location of the coils on the binnacle, and also because these are the headings on which the coils are individually compensated. Thus, the magnetic component of an intercardinal coil which is in the forward-port/aft-starboard direction is called the northwest-southeast component and the coil which produces it is called the northwest-southeast coil. Similarly, the magnetic component produced in the forward-starboard/aft-port direction is called the northeast-southwest component and the coil which produces it is called the northeast-southwest coil.

Types of Compass Coils

In connection with the actual construction details of the various compass coils, it is sufficient to illustrate each coil type, point out the reasons for that particular design and mention the limitations in each case, and then proceed to a discussion of some of the engineering problems encountered.

The type A compass coil set, consisting of multiconductor cable wrapped about the binnacle as necessary to furnish the required compensation, provided temporary compensation for dozens of different binnacle sizes on hundreds of merchant vessels until new construction binnacles were standardized on naval and merchant vessels. Figure 4 illustrates a type A heeling coil on a merchant vessel binnacle.

The "Mark I" coil, which is another unusual arrangement tried experimentally at first, involved the use of a large vertical coil moved physically about the binnacle to line up the coil with the degaussing field at the compass. The cumbersome size and method of physical alignment with magnetic effect, led to an early abandonment of this design.

The first standardized coil assembly was the type B unit, illustrated in Figure 5. It will be noted that this design employed the soft-iron correctors of the compass as cores for the B and C compass-compensating coils. When the size or location of the Flinders bar or quadrantal spheres was changed, the compass had to be recompensated and the ship reswung for new deviation curves.

Figure 4. Type *A* heeling coil on merchant ship binnacle

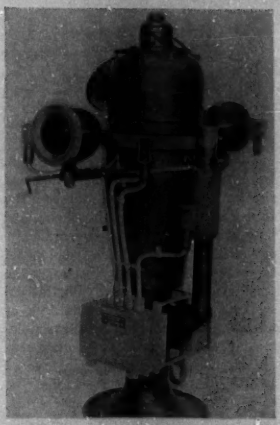


Figure 5. Type *B* compass coils on Navy "Mark VII" binnacle

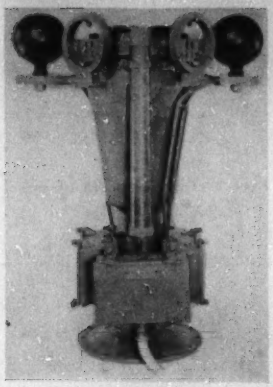


Figure 6. Type G compass coils on Navy "Mark VII" binnacle

Figure 7. Type K-2 compass coil on "Mark VII" binnacle

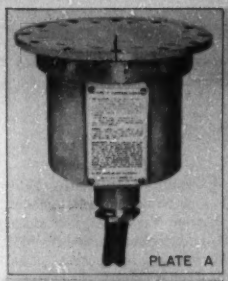
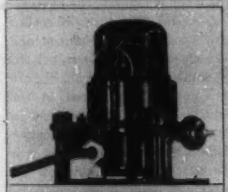


PLATE A



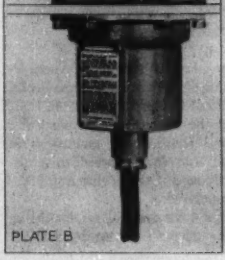


PLATE B

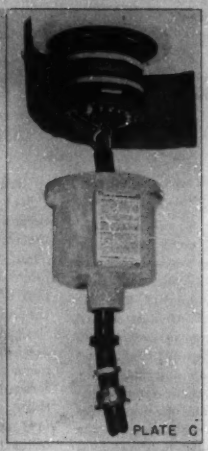


PLATE C

Figure 8. Type T compass coil mounted below magnesyn compass transmitter

Plate A—Front view
Plate B—Coil mounted with magnesyn transmitter and special mounting bracket
Plate C—Type T coil disassembled

To make degaussing compensation less affected by change in position and size of the normal soft-iron compass correctors, a system of coils mounted intercardinally rather than cardinally, was devised. These coils, designated type *G*, are illustrated in Figure 6. The only troubles encountered with this design were a fringe effect and lack of mechanical rigidity, both of these troubles causing no concern under all normal conditions. When degaussing fields, equal in strength to three or four times the earth's horizontal field, are compensated, this fringe effect becomes important, particularly if the *G* coils are close to the compass. This fringe effect is caused by a greater concentration of field around the edges of the coil rather than near the center and is eliminated on later designs by either lowering the intercardinal coils (if they are used in pairs) or using the return rather than the direct field.

An unfavorable condition common to both type *B* and type *G* coils is the possibility for misalignment. This is solved on subsequent designs by assembling all coils into one unit so that the three sets of compensating components remain permanently at right angles.

The type *K* arrangement employs rectangular intercardinal coils hung beneath the heeling coils as illustrated in Figure 7 to produce a direct field for horizontal compensation. The whole assembly is enclosed in a drawn metal case which fits over-all standard-shaped binnacles. This arrangement is standard for all large-size direct-reading compasses. The heeling coil appears in Figure 7 just below the top of the binnacle and the intercardinal coils in the downward extensions adjacent to each sphere.

The type *T* coil assembly, illustrated in Figure 8, employs three sets of windings which are concentric, mutually perpendicular, and wound on a common cylindrical core. This triad coil unit is in a cast aluminum enclosure which is mounted beneath the compass to be compensated. Primarily, it is intended for mounting directly beneath remote-reading magnetic-compass transmitters.

The type *R* coil assembly, illustrated in Figure 9, is mounted inside the binnacle, close to the magnetic compass. The type *R* and type *T* arrangements differ in principle from the type *K* arrangement in that the return field, rather than the direct field, is employed for horizontal compensation.

The shallow, wide R-coil, shaped like a doughnut, is tailor-made to fit inside the binnacle of a new design compass and around the compass corrector magnet assembly, thus taking virtually no additional space.

Current Control for Compensating Coils

In order for the compensating coil field to be always proportional to the degaussing coil current, the compass-compensating circuit supply voltage is tapped off the degaussing-coil series resistor or across a number of turns of the degaussing coil. This voltage is impressed upon the type *A* control-box resistors as shown in the left-hand diagram of Figure 10. The compass coil windings are tapped off the resistance windings.

Figure 9. Disassembled view of type *R* coil showing coil arrangement (left) and cover (right)

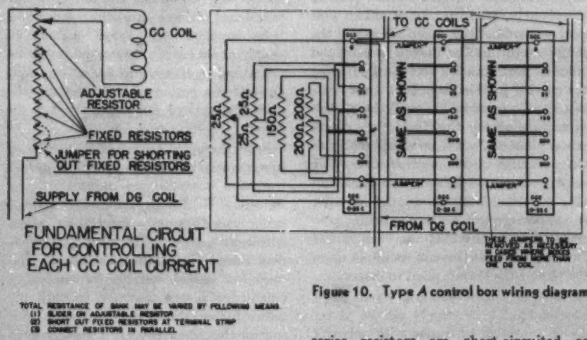


Figure 10. Type *A* control box wiring diagram

Control Box

The type *A* control box is used to set the compass coil currents. It consists of a watertight enclosure having a removable cover and contains three sets of control resistors for compensation. Each set is connected to one terminal strip, as shown in Figure 10, on which the resistance values are marked.

Each set of control resistors permits reduction of voltage from that of the supply leads from the degaussing coil to the voltage required for a particular compass coil winding and consists of three fixed resistors in series with a variable resistor. The entire control circuit is connected across the supply leads from the degaussing coil. Part or all of the series resistors are short-circuited as necessary, using jumpers supplied with the control box, to obtain the minimum voltage across the variable resistor that will permit fine control of the current to the compass coil winding. Since the degaussing field is seldom in line with any of the three compass coil axes (particularly when intercardinally mounted coils are employed) the three control circuits are connected in parallel across the supply leads from the degaussing coil, one circuit being used for each of the three magnetic components. As a result, one control box is normally required for each degaussing coil affecting the compass.

Engineering Problems

Use of a ferrous core to reduce space requirements presents a few interesting problems not encountered in ordinary coil design. If the coil with ferrous core is mounted close to the compass, interaction between the compass needles and the ferrous core occurs. The induction in the core from the compass needles results in quadrantal compass deviations (four alternate positive and negative half cycles, changing in accordance with the sine or cosine of twice the angle of heading). Increasing separation between core and compass magnets, in order to decrease interaction, results also in an increased size of the compass coil so that very little, if any, net reduction in size and weight results from the use of ferrous cores. If used, the core material must have very low hysteresis when magnetized sufficiently to produce fields at the compass between $\pm 1/2$ gauss (the scarcely measurable nonlinearity of one milligauss, when present for example as a hysteresis effect, results in the very appreciable error at the compass of 1/4 degree).

The problem of waterproofing coils for use in all weather deserves special attention. If the coil is small, like a type *T* coil, cast-aluminum housings with covers screwed tight against gaskets prove very effective, but on larger designs requiring drawn metal enclosures, the problem of obtaining satisfactory soldered joints is serious. Even with tin-plated joints, exacting fits, meticulous soldering, and subsequent pressure tests, the joints cannot be depended upon with absolute certainty. Apparently, the only way to insure watertight integrity of such coils is by the use of a suitable filling compound, applied after assembly and wiring are completed. Although this adds dead weight to the assembly, all units which have been properly filled performed excellently without failure.

While degaussing coils produce fields at the magnetic compass of magnitudes that would make readings entirely unreliable, compensation by compass coils used in conjunction with the degaussing installations reduces resultant deviations to within a few degrees. Allowance for the small remaining uncompensated portion of the deviation may be made by the navigator by means of a deviation card (a tabulation of the deviations on various headings). The use of several different prefabricated types of compass coils allows for simple installation on nearly all binnacles.

No Discussion

Use of Propulsion Generators on Naval Vessels to Supply Shore Power

F. E. ANDERSON
NONMEMBER

W. H. FIFER
FELLOW AIEE

THE USE of propulsion generators on naval vessels for supplying emergency shore power has interested electrical engineers for many years. During World War II the Navy modified several electrically propelled ships to make them suitable for quickly supplying shore power to areas where the retreating enemy had destroyed or sabotaged existing shore plants. However, the modified vessels are not available or practicable for supplying shore power for indefinite periods to relieve existing shore generator capacity.

Historical Background

Ever since the turbine electric propelled aircraft carrier, *USS Lexington*, was used in 1929 to supply emergency shore power to the city of Tacoma, considerable interest has been shown by public utilities in the obtaining of emergency power from propulsion generators on naval vessels. The *USS Lexington* was used for one month to supply a load of 20,000 kw to make up a power deficit caused by temporary lack of water power. A step-up transformer was used to connect the 5,000-volt output of one propulsion generator to the 12,500-volt line of the power company. The transformer and necessary switchgear were mounted on a pier to which the vessel was moored and necessary additional control equipment was installed temporarily in the vessel's engine room.

Early during World War II, the use of naval vessels' propulsion generators for emergency shore power assumed high importance due to the need for ready availability of power generating facilities to replace or supplement that destroyed or damaged by the enemy when retreating. Also, during World War II, there were constructed a large number of a-c turbine electric propelled vessels which had possibilities for supplying shore power.

One of the most notable instances of wartime requirements for emergency shore power was at the French port of Cherbourg in the summer of 1944. This port was one of the Allied targets in the initial assault on the European mainland since its port facilities were vitally needed for moving in Allied supplies. In retreating, the Germans had severed the 33,000-volt transmission line from the central power plant at Caen from which nearly all the power for Cherbourg had come. The only generating capacity remaining available to the Allies was an old standby steam power plant with two turbine generators, each rated at 900 kva. The Germans had left these units intact upon the plea of the French plant manager.

However, the Cherbourg steam plant was inadequate to supply all the power needs and, while operating with only one generator on the line, the unit was overloaded and burned out. The confused engineer on watch transferred the load to the second unit and burned it out, thereby leaving the entire area without any power, except for a few low-rated emergency generator sets. The situation was desperate and the success of the whole military operation hung in the balance.

In seeking generating capacity for Cherbourg, it was remembered that the *USS Donnell*, a United States Navy destroyer escort vessel with turbine electric a-c propulsion, was tied up in a British channel port awaiting disposition since 60 feet of the vessel had been blown off the stern by an enemy torpedo. An examination of the vessel showed that the propulsion equipment was in good condition and the vessel was towed to Cherbourg for possible use as an emergency power supply.

However, a transformer suitable for matching the 1,500-volt output of the propulsion generator at 50 cycles with the shore power line of 5,000 volts was required. A new transformer which the Germans had apparently overlooked was found in the French Naval Arsenal at Cherbourg. The transformer had a 6-to-1 ratio but upon opening it was found to have a center tap which would make it possible to reconnect for a 3-to-1 ratio, which was suitable to tie the propulsion generator to the shore system. The transformer was set on the pier alongside the vessel and within a short time power was being supplied. The vessel was able to supply about twice the rated power of the old standby steam plant and supplied power for a period of about six months until the normal power supply was restored.

Several months prior to the Cherbourg power emergency, the Bureau of Ships had started a study to determine the most suitable class of ships and the best type of equipment to supply emergency power whenever needed. The Cherbourg incident demonstrated very conclusively the need for vessels suitable for supplying emergency power to move in with invasion forces. The study showed that the turbine electric a-c propelled escort vessels were the best suited for this purpose and by September 1944 a contract was awarded to provide sufficient equipment to convert 15 of the escort vessels into emergency power vessels. The conversion equipment included a variable ratio transformer, switchgear, control and protective equipment, and high-voltage power cable which would allow a vessel to supply shore power at almost any point in the world. This equipment was designed especially with the thought that emer-

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F. E. Anderson and W. H. Fifer are both with the Bureau of Ships, Navy Department, Washington, D. C.

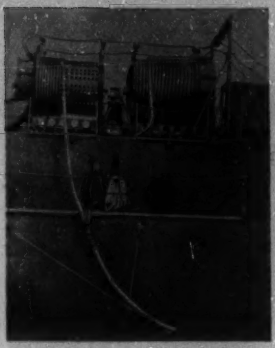

Figure 1. Close-up view on starboard side of destroyer escort vessel showing ship-to-shore cable reels and unit substation

gency shore power would be in great demand upon invasion of Japanese-held industrial areas and in the Japanese homeland itself.

The first vessel on which this special equipment was installed was the *USS Wiseman*. The installation was made in December 1944 by the Charleston naval shipyard at Charleston, S. C. and the unit was tested by tieing into the South Carolina Power Company system. This installation is illustrated by Figures 1 and 2. This unit proved to be entirely successful in these tests, and this initial installation was soon followed by similar installations by the Pearl Harbor naval shipyard on the *USS Whitehurst* and the *USS Marsh*, both a-c turbine electric propelled destroyer escort vessels, and sister ships of the *Wiseman*.

The value of the installation of this shore power conversion equipment was immediately demonstrated very conclusively. In April 1945, the United States forces moved into Manila, but the Japanese in retreating burned and sabotaged the city quite thoroughly with the result that a total of only 2422.5 kva of generating capacity was available in the city and this was in 14 emergency generator sets all over the city. Initially, the 14 sets were operated in parallel; however, in order to provide a more dependable power source, the *USS Wiseman* was sent to Manila and tied into the shore power system at 13,200 volts. The *Wiseman's* propulsion generators took over the entire load of the city and supplied a maximum load of 3,000 kw until the normal supply could be placed back in service.

Subsequent military successes in the Pacific, terminating in the end of hostilities in August 1945, obviated the need for additional shore power vessels and, as a result, no further installations of this equipment were made on the escort vessels.

In November 1945, the *USS Whitehurst* and *USS Marsh* were assigned to Guam to supply power for dredging operations in Apra Harbor. These vessels were operated in parallel with the power plants on shore, but operating difficulties were experienced due to voltage regulation in supplying power to the dredge. Special regulators were furnished for this service to provide for satisfactory operation of these vessels with the dredge. In order that the *Marsh* could be released for operation with the fleet, the Bureau of Yards and Docks outfitted the *USS Saranac*, an a-c turbine electric propelled tanker to supply power for dredging operations at Guam until the permanent shore power facilities could be completed. The *Marsh* was subsequently sent to Kwajalein during May 1946, to supply shore power at that location when the shore generating facilities failed during the preparations for the "Crossroad" atomic bomb tests at Bikini Atoll.

It became apparent soon after the end of the war that generating capacity in the United States was being taxed with an ever-increasing load, and the Navy considered that the shore power units should be kept in readiness for possible emergency use. Since the installation of the substation equipment in a destroyer escort vessel resulted in a reduction in its military effectiveness and added a considerable amount of topside weight which was not desirable, it was decided to retain the remaining substations on shore and, when needed for emergency use, to set the unit up on a pier or barge, with the vessel moored alongside much in the same manner as the *USS Lexington* and *USS Donnell* had been used. In order that a number of these vessels might be in readiness for such duty, the installation of control and power cables on a number of vessels of the turbine electric escort type was authorized. With the installation of these cables, the vessel could be readily connected to the unit substation mounted alongside and supply shore power.

The first experience in the use of this type of installation was obtained in New York City in May 1946. Power production for New York City was imperiled by a coal strike. Since lack of coal would shut down nearly all railroad and harbor tug traffic, the bulk of the city's food sup-

Figure 2 (below). Starboard view of destroyer escort vessel, *USS Wiseman*, showing ship-to-shore equipment installed on vessel

Note cable reels on top deck and unit substation immediately forward of cable reels

Figure 3 (right). The *USS Foss* and *USS Maloy* supplying power at South Portland, Maine

Note unit substations on pier between vessels. Power cable from Maloy to unit substation shown also

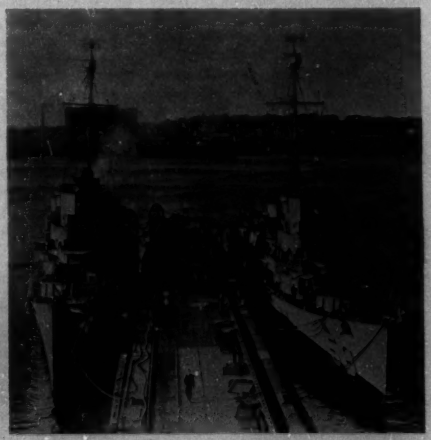

Figure 4. Power supply connections from USS Maloy to shore in supplying shore power at South Portland, Maine

Figure 5. Details of method used to make shore power connections from vessels in supplying shore power at South Portland, Maine

Portable power cables operated at 1,700 volts from vessel's generators. High-voltage side of unit substation operated at 11,500 volts. Three-conductor high-voltage leaded cable used to tie in between pier and shore transmission line

plies would have to come in by way of motor truck through the Holland and Lincoln vehicular tunnels, and it was imperative that power be available for ventilating these tunnels. In anticipation of the requirement for this emergency power, the destroyer escort vessels *USS Foss* and *USS Maloy* were ordered to have the shore power wiring installed and to be put in readiness to supply power to the tunnels. The installation was completed with the unit stations and high-voltage cable installed on barges for use alongside the vessels. Fortunately, the coal strike ended in time and the vessels were not required to actually supply power for this purpose, but the equipment was tested at the New York Naval Shipyard and operated satisfactorily, demonstrating the practicability of the use of such an arrangement.

The Navy has a large number of various types of vessels which have been placed in an inactive status and berthed in groups at various locations along the coasts. In Green Cove Springs, Florida, such a berthing area is provided for a number of vessels, but no shore power is available to operate dehumidifying machines, lights, and other essential electric services for these vessels. A high line is now being erected from Jacksonville to Green Cove Springs, but during the interim period, two destroyer escort vessels, the *USS Harmon* and the *USS Whitehurst*, have been supplying shore power.

During the fall of 1947, a serious drought in the New England states, especially Maine, drastically reduced the hydroelectric power output and resulted in an emergency situation insofar as the use of electric power was concerned. In November 1947, the *USS Maloy* and the *USS Foss* were assigned to temporary duty at Portland, Maine, in order to supply emergency power for the processing of foodstuffs, especially potatoes. The vessels were docked at a pier in a South Portland shipyard and the unit substations placed on the pier alongside the vessel. This installation is illustrated by Figures 3, 4, and 5. Due to the high tides at South Portland, it was necessary to provide cable gantries and sufficient slack in the cables to accommodate the rise and fall of the tide, as shown in the illustrations. The secondary of the substations supplied power to the Central Maine Power Company at 11,500 volts. The power output from the two vessels was about 9,000 kw and was cascaded up to Aroostock County in Northern Maine through the lines of the Central Maine, Bangor Hydro, and Maine Public Service power companies. These vessels supplied this load satisfactorily for a period of about five months.

Technical Considerations

In adapting the vessel's propulsion generators to shore power service, there are several differences between the use of the generators for propulsion service and for shore power supply purposes which must be considered. These include the following:

1. The generators supply only one or two loads for propulsion service as compared with a large number of loads in shore service. All of the Navy a-c turbine electric drive vessels with the exception of the old electric propelled battleships and aircraft carriers have either one or two propellers each driven by a propulsion motor with an associated propulsion generator. If the vessel has only one propeller, only one propulsion generator and motor are provided, and the only load supplied from the propulsion generator will be the propulsion motor, except in the case of certain tankers on which power is supplied also to the cargo pumps for taking on and discharging cargo. If the vessel has two propellers, as is the case of the destroyer escort vessels, two propulsion motors and generators are provided, and each generator can supply power to one or both motors depending upon the vessel speed desired. With one generator supply-

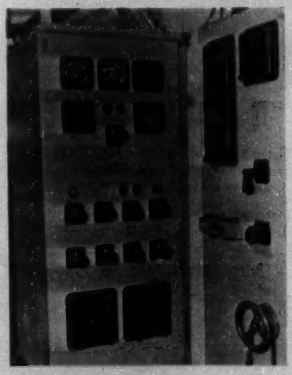

Figure 6. Ship-to-shore power control cubicle located in forward engine room of a destroyer escort vessel

ing both motors, the other generator is not supplying power. The generators are not operated in parallel.

2. Variable frequency is used for propulsion service as compared with constant frequency for shore service. Since synchronous propulsion motors are used to drive the propellers, the motor speed is varied by varying the generator frequency. This requires regulation of the generator voltage on a constant volts per cycle basis rather than for constant voltage as required for shore service.

3. No circuit breakers are used in propulsion service. Since the propulsion generator and propulsion motor form a complete power system, the power to the motor can be cut off by de-energizing the complete power system which is done by removing the field excitation from the generator rather than by disconnecting the motor by the use of circuit breakers.

4. Propulsion generators are not operated in parallel. As pointed out in paragraph 1 each generator forms a separate power system and does not require operation in parallel with another generator. This eliminates the need for paralleling equipment which is required normally in the operation of a power plant.

In order to utilize the propulsion generators on the a-c turbine electric destroyer escort vessels, shore power conversion equipment, referred to previously, was provided. This equipment provided control panels for each of the vessel's two engine rooms, and a power transformer with switchgear equipment for installation on the vessel or on a pier or barge alongside.

The control panels each contain synchronizing equipment, including phase rotation lamps and a synchroscope, to enable the propulsion generators to be paralleled with each other and with the shore supply when tieing into an energized system. Provided also on this panel are meters for reading the generator kilowatts and volts. The control panel is installed adjacent to the vessel's propulsion control panel as illustrated in Figures 6 and 7.

The power transformer is a 3-phase forced-oil-cooled transformer with variable voltage secondary. The primary is connected in delta for 1,725 line volts at 60 cycles, or 1,450 line volts at 50 cycles. A voltage adjusting handwheel is provided so that the primary voltage can be reduced in about $2^1/_2$ per cent increments to 1,552 volts at 60 cycles and in about 3 per cent increments to 1,277 volts at 50 cycles. The secondary is provided with adjustable links which will provide 7,200 and 21,600 volts in a delta connection and 12,500 and 37,500 volts in a Y-connection. A tertiary winding is included also which will permit operation at 2,400 volts delta and 4,160 volts Y, but the power output at these voltages is only 1,667 kva as compared with 4,500 kva at the higher voltages and 60 cycles. The power output at 50 cycles at the higher voltages is 3,750 kva. Provision is made also in the tertiary winding for bringing out the neutral of the Y-connection to provide a 4-wire system. The secondary voltages were chosen after a study of the usual transmission line voltages thoughout the world in order that the unit might be tied in with a power system at most any point in the world, with especial consideration given to the Far East.

The switchgear consists of two General Electric magne-blast AM 5-100, triple-pole circuit breakers, single-throw type, rated 5,000 volts, 1,200 amperes with 100,000 ampere interrupting capacity. The circuit breakers are located on the generator side of the transformer, and each circuit breaker enables its associated generator to be removed from the line. The circuit breakers are of the metal-clad draw-out type and have remote closing from the engine room control panels. The propulsion equipment has phase balance and ground protection when operated to provide ship propulsion. These phase balance and ground protective relays, when operated during propulsion service, trip open the field circuits of the generator, de-energizing the propulsion system. For shore power service, additional protection is provided in the form of overload and reverse power protection, and the four protective relays phase balance, ground, overload, and reverse power, are placed in the circuit breaker trip coil circuit to trip out the circuit breakers in the circuit in which the fault occurs.

The switchgear and transformer with the required auxiliaries are assembled together to form a unit substation of the outdoor-type as illustrated in Figure 8. The total weight of this unit with oil was designed to a limit of 45,000 pounds in order that it might be handled by crane facilities available aboard Navy tender ships.

The power rating of the substation is based upon the rating of the generator operating at 3,600 and 3,000 rpm. The generator stator and rotor heating determines the maximum output of the generators. For propulsion service, the generators are rated at 4,600 kva, 5,600 rpm, and 93.3 cycles, but, slowing the unit down to 60 and 50 cycles, the ventilation is reduced since these units are self-ventilated. This reduced ventilation requires a reduction in the field and stator amperes in order to prevent overheating.

In order to provide for running the high-voltage lines from the transformer secondary to the shore in those cases where the lines must be carried over water, special high-voltage submersible cables were designed. One type cable is rated at 13,500 volts and the other type rated at 37,500 volts. Cellular rubber floats are provided to be attached to the cable to provide the necessary buoyancy to float the cable from the barge or vessel to the shore as illustrated by Figures 9 and 10. Special motor-driven reels are provided for this cable along with a foundation structure which will accommodate two 1,000-foot reels of cable. One reel of 13.5 kw cable and one reel of 37.5 kv cable have been provided for each unit substation equipment. The two cable reels and supporting structure as mounted on a vessel are illustrated in Figure 1.

For propulsion service, voltage regulation is provided by means of an amplidyne regulator which maintains constant volts

Figure 7 (left). Ship-to-shore power control cubicle located in after engine room of a destroyer escort vessel, showing the vessel's propulsion control cubicle on the left

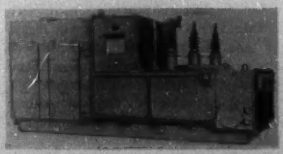

Figure 8. Front view of unit substation from cooler compartment end, showing high-voltage bushings, ratio adjusting handwheel, and water-tight doors to the circuit breakers on the left

Figure 9. Submersible high-voltage cable with floats attached

per cycle. This type of regulator will allow proper voltage regulation for a shore power system which requires constant voltage regulation provided the frequency is held constant. However, this regulator will not provide for proper reactive kilovolt-ampere load division between two generators which are paralleled on a bus without any reactance between the two generator terminals.

In the design of the shore power unit, it was considered that manual regulation would be satisfactory since the units would supply loads which did not have large instantaneous load variations and this has proved to be the case with the exception of the shore power operation at Guam. In this instance, the load was a dredge with a 1,000-hp induction motor-driven cutter and a 3,000-hp pump. When using only one destroyer escort vessel to supply power, starting of the 1,000-hp cutter motor caused the voltage at the dredge to drop about 50 per cent which opened the dredge circuit breaker and made it impossible to start the dredge machinery. Operation was satisfactory when the vessel was paralleled with the shore supply of 1,400 kw which had voltage regulators, but this type of operation proved to be too difficult since it was necessary to provide manual division of load and reactive kilovolt-amperes and maintenance of system voltage at three power generating stations by telephone communication under conditions of fluctuating load. In order to provide proper voltage regulation and division of reactive kilovolt-amperes for the vessel's two generators when operating in parallel, the regulators used for propulsion service were modified on each of the three destroyer escort vessels on which the permanent installation of the substation equipment had been made. This modification consisted of providing for division of the reactive kilovolt-amperes by means of cross-current compensation between the two generators and providing compensation for transformer reactance voltage drop and cable resistance voltage drop in order to obtain better voltage regulation at the load.

The unit substation, including the transformer and switchgear, and the engine room control panels, were supplied by the General Electric Company. The high-voltage cable was supplied by the Okonite Company, and the cable reels and supporting structure were provided by the Norfolk Naval Shipyard. The cable floats used for the high-voltage cable were furnished by the Sponge Rubber Products Company.

Operational

In the operation of these units, there are several factors which must be considered in order to insure satisfactory operation of the propulsion plant. The vessel must be located in a sufficient depth of water so that the condenser intakes will not become clogged with silt. In order to minimize the possibility of silt clogging the condensers, the vessel should have a depth of at least five feet of water below the keel at low tide. When operating in shallow water, the condenser tubes should be blown every six hours. Since the maximum power which can be delivered from the substation is only one-half the full power rating of the propulsion machinery, the full power can be supplied from one boiler, if necessary, for a short period while routine maintenance work is carried out on the other boiler and thus maintain continuity of power supply.

In preparing an operational schedule for the operation at Portland, Maine, referred to previously, it was considered that the vessels would supply power for six days a week, Monday through Saturday, and routine maintenance would be carried out on Sunday. However, the performance of these plants was so creditable that it was not necessary to shut down every Sunday for maintenance. The operating score for the four plants on these vessels showed an operating time of a total of 10,741 turbine hours with only 791 hours off the line, or an availability of 92.5 per cent. The majority of these 791 hours out of service were for normal planned maintenance at times when the power system loads were low. A total of 24,220,000 kilowatt-hours, or an average of 4,500 kw per vessel, were supplied during this period. The vessels are able to supply their own ship's power requirements from the ship's service power plant, making it unnecessary to provide any shore power services to the vessels.

The cost of power production by naval vessels is high due to the fact that the vessel must be fully manned, thus entailing a large operating crew. The total cost of power from a vessel in active service,

Figure 10. Submersible high-voltage cable with floats attached running to power station ashore

including both the direct charges of power production and the fixed charges, would be several times the cost of power from a shore power plant. If the vessel were taken from the Reserve Fleet, the total cost of power would be further increased since the reactivation and represervation cost must be included also.

Conclusions

Operating experience has shown that it is practicable to use propulsion generators on naval vessels to supply emergency shore power. However, it should be pointed out that the propulsion generators and associated equipment on naval vessels are not designed as a source of shore power supply. The primary mission of a naval vessel is as a fighting vessel and all equipment is designed to perform most efficiently in this function. For that reason the frequencies and voltages of the generators are quite likely not to be those commonly used for power on shore, as in the case of the destroyer escort vessels. This results in not being able to utilize fully, when supplying shore power, the generating capacity of the plant and requires that considerable auxiliary equipment be added to enable the vessel to supply shore power.

There appears to be a popular misconception that any large naval vessel is capable of supplying appreciable amounts of shore power. This assumption is probably due to the publicity given when the *USS Lexington* was used to supply 20,000 kw of shore power to Tacoma, Washington, in 1929. The *Lexington* and her sister ship, the *USS Saratoga*, both of which have been lost at sea, were the only modern United States aircraft carriers which have ever been equipped with electric propulsion. The only battleships which are fitted with electric propulsion are the *Maryland*, *California*, *West Virginia*,

Colorado, and *Tennessee*. However, the electric propulsion plants on these vessels are designed for operation at approximately 35 cycles and are therefore not suitable for supplying 50 or 60 cycle shore power. No other aircraft carrier, battleship, or cruiser is fitted with electric equipment suitable for supplying shore power.

With the exception of the vessels in active service for training, naval maneuvers, and special assignments, the majority of the destroyer escort vessels employing electric propulsion have been inactivated and placed in the reserve fleets. To use a destroyer escort vessel for shore power purposes would involve removing a vessel from its assigned duties with probable interruption to the entire fleet or force to which it is assigned or would require a vessel in the reserve fleet to be reactivated. To reactivate a vessel not only requires time and money, but requires a new crew to be assembled to operate it.

It has therefore been the past policy of the Navy Department to authorize destroyer escort vessels to supply shore power only when an emergency involving health or conditions affecting the nation as a whole are involved. The contemplated supply of power to ventilate the Holland and Lincoln tunnels in New York and the supplying of shore power to the state of Maine to process food stuffs are examples of where use as shore power supplies have been approved. It should be especially noted that the vessels are used only to carry over some acute emergency and have not been authorized for indefinite use merely due to shortage of generating capacity. The relatively small output combined with the need of the vessels in the fleet, the lack of crews and cost of reactivating the vessels, makes them entirely impractical for shore use except in extreme emergency conditions.

Discussion

L. W. Bates (Captain U. S. N. R., Ret., Navy Department, Washington, D. C.): The use of propulsion generators on Navy ships to supply shore power is subject to limitations since the voltages and frequencies at which they normally operate are not suitable for use on shore. This requires the installation of special equipment and also reduces the amount of power available for shore use considerably below the rated capacity of the generators.

The armed forces however, have, or had, a number of mobile generating plants which were designed especially for furnishing shore power in emergencies at standard voltages and frequencies. The majority are 60-cycle plants but a few were arranged for operation at either 50 or 60 cycles.

The largest plants of this character were four special barges, each having a capacity of 30,000 kw, designed and built with funds supplied by the Defense Plant Corporation and operated by the Department of the Army. Primarily intended for use on the inland waterways of this country, they had a draft of only nine feet. Later developments however, required their use abroad so that certain structural changes were made to three of the plants including the installation of bows on the barges, to facilitate towing across the ocean.

These plants use either oil or gas as fuel and the generator voltage is 13,800 with a transformer for stepping up the full output to higher voltages suitable for interconnection with existing high-voltage transmission systems on shore.

It is of passing interest to note that these plants were given the following names: *Seapower*, *Inductance*, *Impedance*, and *Resistance*.

Three of the plants were declared surplus after the cessation of hostilities and were sold by the War Assets Administration and the remaining one is still owned by the Government and is in use in the Far East.

The Department of the Navy has one floating power plant, the *Jacona*, of 20,000 kw capacity which is now about 15 years old. It was built originally for one of the New England power companies, was commandeered during the war and later towed to Pearl Harbor to supplement the generating capacity of the public utility company in meeting its war-time demands pending completion of the installation of a 40,000-kw generating unit in one of its plants. This vessel is now supplying emergency power in the Far East.

The Navy also has two 10,000 kw railway mounted power plants, each comprising a train of six cars as follows: turbine car, boiler car, switchgear car, transformer car, and two cars for various auxiliaries, one of which is a gondola car and the other a box car. One plant is located on the west coast and the other in the east from where they can be transported at short notice to any naval establishment within the continental limits if a sudden emergency power demand should develop. These plants burn oil and voltages of 2,400, 4,160, 11,000 and 13,800 are available to permit interconnection with the electric system of any naval activity.

Fortunately there were no failures of the normal power supply for naval establishments during the war nor were there any instances of power damage due to sabotage or bombing so that these plants were not used directly for military purposes. They were however, used indirectly to contribute to the war effort when they were at times leased to a synthetic rubber plant and to several public utility companies whose normal power output was seriously curtailed due to drought conditions. During such periods as the plants were leased, it was of course understood that they would be returned immediately to naval custody at any time if needed to meet emergency naval needs.

A complete description of the railway mounted plants with illustrations was given in a paper before the American Society of Mechanical Engineers in December 1944.[1]

The armed forces also have a number of smaller mobile Diesel-driven generating plants mounted on railway cars and truck trailers which have been of material assistance in many cases.

It is expected that additional mobile power plants of various types and sizes will be obtained from time to time to be available for future emergencies and that consideration will be given to the possible use of gas turbine driven generators in some cases.

Reference

1. 10,000 Kv Railway-Mounted Mobile Steam Power Plants for the United States Navy Department, Bureau of Yards and Docks, **Edwin Lundgren, L. R. Biggs.** *Transactions*, American Society of Mechanical Engineers (New York, N. Y.), volume 67, pages 141–65.

F. M. Starr (General Electric Company, Schenectady, N. Y.): This paper is especially noteworthy in clarifying in concise terms the limited degree to which propulsion power can be made available for shore installations.

Even if electric propulsion power were abundant, it normally could not be justified for shore use under any except emergency conditions. All components of cost are high: power and energy losses are high; maintenance is high; and operating efficiencies are low.

In recent years, the power companies have shown considerable interest in mobile floating power plants designed for the purpose and not necessarily utilizing propulsion generators. They have many attractive features not only for emergency outages, but also to take care of seasonal peaks, water shortages, unanticipated load growth, and so forth. Such applications have gained little headway, however, primarily because of the high initial cost.

The authors have discussed in detail the satisfactory performance of the turbine-electric destroyer escorts equipped with General Electric shore-power conversion equipment to meet war-time power emergencies and subsequent peacetime emergency applications. Late in 1944, the General Electric Company accepted contracts to design and manufacture on a rush basis, the necessary conversion equipment to permit rapid transfer from propulsion to shore power on 15 destroyer escorts.

One of the major problems was to incorporate enough flexibility in the equipment to insure adequate parallel operating performance at both 50 and 60 cycles and at a number of operating voltages, the exact values of which could not be known in advance.

This was solved by arranging for six nominal output voltages with four primary full capacity taps on the transformer fully co-ordinated with the available range in field currents on the generators.

The preliminary tests on the first ship at the Charleston Naval Shipyard demonstrated that:

1. The two propulsion turbines and generators could be operated successfully in parallel.

2. Uniform kilowatt load division could be obtained provided the propulsion speed levers were equipped with vernier position adjusters.

3. The two ship's turbines could be paralleled successfully with a large power system even though the nominal voltage of the ship's output and the operating voltage of the power system were substantially different.

4. Balanced reactive kilovolt-amperes could be successfully maintained by manual field control.

5. The ship's generators could be operated at a leading power factor of 90 per cent (obtained with reduced fields) without any danger of instability.

6. Temperature rises expected at rated kilowatt and kilovolt-amperes outputs of the generators and transformer were not exceeded.

As noted by the authors, difficulties were encountered in dredging operations at Guam and an attempt was made to correct them by the installation of voltage regulators. However, subsequent analysis of this particular problem indicates that there was not sufficient kilowatt capacity to handle the power surges which are inherently characteristic of dredge pumps. One of the important limitations on the application of the propulsion power of a small vessel for shore use is the size of the larger motors which must be started. Vessels such as the destroyer escorts are best suited to small diversified loads.

Origin of the Electric Motor

JOSEPH C. MICHALOWICZ
MEMBER AIEE

THE DAY that man molded the first wheel from the sledlike skids of his primitive wagon should be one of great commemoration, had not its identity been lost in the passing of time. Not unlike the wheel and probably second only to the wheel, the electric motor has been a great benefactor to man and its history, too, slowly is being forgotten.

Today, we hear very little, if anything, about Thomas Davenport, the blacksmith who invented the electric motor; or about De Jacobi, who propelled the first boat by means of an electric motor; or of Charles Page who successfully carried passengers on the first practical electric railway. Had it not been for the efforts of these men and others like them, the benefits of the electric motor probably would not be enjoyed today. It is the purpose of this paper, in general, to trace briefly the early history of the science of electromotion and, in particular, to bring to light and to honor the inventor of the electric motor.

Early History

The development and rapid growth of our electrical science and the electrical industry can be followed very easily through the growth of the basic principles. The basic principle governing the operation of the electric motor is, of course, that of the production of motion by the use of magnetic fields. Legend tells us that the first exercise of this knowledge probably is accredited to Hoang-Ti, the mythical founder of the Chinese empire, who was the first to construct a magnetic compass in 2634 B. C. Factually, the citation delivered by Andre-Marie Ampere in 1820 A.D. of the nature of the electric current and its relation to magnetism was the classic truth that was necessary for the development of the practical motor. Not quite a year later, Michael Faraday, using this information, produced rotation of a wire carrying electric current, around a magnetic pole. But it was not either of these two illustrious men who deserve credit for the development of the practical electric motor. There were others, some talented, some not, some prophetic, some curious, who, through discouragements and failures, were directly responsible in giving us the modern electric motor. Briefly, these early achievements up to the year 1850, may be listed as follows:

1821—Michael Faraday demonstrated for the first time the possibility of motion by electromagnetic means with the movement of a magnetic needle in a field of force.

1829—Joseph Henry, a teacher of physics at the Albany Academy in New York, constructed an electromagnetic oscillating motor but considered it only a philosophical toy.

1833—Joseph Saxton, an American inventor, exhibited a magnetoelectric machine before the British Association for the Advancement of Science.

1837—Thomas Davenport, an American inventor, obtained the first patent on an electric motor.

1838—Solomon Stimpson, an American, built an electric motor having 12 poles and a segmental commutator.

1839—Moritz-Hermann De Jacobi, a Russian scientist, demonstrated the use of the electric motor to propel boats.

1840—Truman Cook, an American, built an electric motor having a permanent magnet armature.

1840—Robert Davidson, a Scot, built a lathe and a small locomotive powered by electric motors.

1845—M. Froment, a Frenchman, built an electric motor similar to a "breast wheel, whose paddles were acted upon by magnetism instead of water."

1847—Moses G. Farmer, an American, constructed and exhibited a 2-passenger car and locomotive driven by an electric motor.

1850—John Little, an American, constructed an electric motor having radially arranged permanent horseshoe magnets.

1850—Charles G. Page, an American, developed a solenoid-type electric motor.

Although these accomplishments and the many others that took place in the years following 1850, helped considerably in the design of our present electric motor, the most outstanding efforts were those of Davenport, De Jacobi, and Page. These are significant because Davenport's machine was, without a doubt, the first practical electric motor; De Jacobi's apparatus demonstrated the first practical application; and the device of Page showed the possibilities of a different motor design.

The Davenport Motor

Thomas Davenport, the rightful inventor of the electric motor, spent a life that seems more fictional than factual. His rigid understanding of the motor's principles, his clear vision of the possibilities of such a device and his driving persistence to establish a foundation for a discovery too early for its time, easily should qualify him as a member in the society of Ampere, Faraday, Morse, Bell, and other immortals of the electrical science. To understand the trials of this early American inventor, it would be unwise not to present at least a slight glimpse into his early life.

That Thomas Davenport came from true American stock, there can be no doubt. His ancestors came to Dorchester, Mass., prior to 1640, a short time after the landing of the Pilgrims. His grandfather was a soldier in the War of the American Revolution and he had two brothers in the War of 1812.

The inventor, one of 12 children, was born in Williamstown, Vt., on July 9, 1802, of poor, though patriotic and reli-

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JOSEPH C. MICHALOWICZ is a professor in the department of electrical engineering, at the Catholic University of America, Washington, D. C.

gious parents. His early boyhood consisted mainly of working the soil and tending the farm, but he did not neglect to obtain the little education that was available in those days. He took advantage of every possible moment in the classroom, even engaging in reading during the recess periods.

At 14 years of age, he became a blacksmith's apprentice. In this trade he was "bound out" for seven long years, with no compensation other than a knowledge of smithing and a 6-week term a year at school. Here again, he occupied his leisure time in reading books that he obtained from "auction" libraries.

At the end of his apprenticeship, though penniless, he started his own blacksmith's shop in the town of Brandon, Vt. He continued his trade for ten years until the year 1833 when his curiosity was aroused by a report that "a galvanic battery" which would "lift a common blacksmith's anvil" was on exhibition at Crown Point, N. Y. He instantly made the trip to Crown Point but was unable to view the device. Later reports informed him that this newly found wonder contained a horseshoe shaped magnet. These accidental references of "anvil" and "horseshoe" had excited him so thoroughly, that he made plans for a second trip to Crown Point. Not having the funds with which to finance this second journey, he persuaded his brother Oliver, a tin peddler, to take him in his peddler's cart.

Arriving at Crown Point, Davenport succeeded in viewing the "galvanic battery" which would "suspend an anvil between heaven and earth." It was an electromagnet invented a few years previously by Professor Joseph Henry of Princeton University. It consisted of a suspended horseshoe magnet about a foot long, wound with silk-insulated copper wire connected to a galvanic cell. It had been purchased by the proprietor of the Penfield Iron Works to separate the iron from the waste in the local low-grade ore. Davenport marvelled at the ability of this 3-pound magnet to raise an anvil weighing 150 pounds, and, imagining that this power could be put to more useful work, asked its purchase price. The price was $75. Again brother Oliver was called upon. Reluctantly, Oliver traded his wares, his cart, and his horse for the necessary $75 plus a worthless nag, and the magnet was purchased.

Having brought the magnet home, Davenport discovered that he could make the magnet lose its power by disconnecting one of the wires to the battery and could restore it again by holding the severed ends together. This simple electrical fact immediately convinced him that the power bestowed upon the magnet was controllable. The starting and stopping of an electric current by the making and breaking of an electric circuit revealed a most important fact, the discovery of which, simple though it may seem today, well could be accredited to Davenport, for there is no record to show that anyone else, even Joseph Henry, had performed such an experiment.

Overwhelmed by curiosity, Davenport began to dismantle the magnet—but not without care; for he summoned his wife, Emily, to take exacting notes of its construction. From these notes, he constructed a larger magnet, using the silk from his wife's wedding dress as an insulator between the conductors. The home-made replica was a success. He built more and larger ones and predicted that "steamboats would be propelled by this power" and that "it would replace the murderous power of steam."

Davenport's next objective was to convert the linear motion of the electromagnet into rotational motion and thereby make the flow of energy continuous. After much experimentation, he finally obtained rotational motion early in 1834. He accomplished this by mounting a magnetized iron bar horizontally on a bearing and held the electromagnet in such a position that its repulsion set the iron bar in motion. Then, by breaking the circuit by hand at properly timed intervals, he found that the iron bar could be kept in continuous motion.

Later, he built a better and larger motor, about which Davenport writes in his own memoirs:

> In July 1834, I succeeded in moving a wheel about seven inches in diameter at the rate of about 30 revolutions a minute. It had four electromagnets, two of which were on the wheel, and two were stationary and placed near the periphery of the revolving wheel. The north poles of the revolving magnets attracted the south poles of the stationary ones with sufficient force to move the wheel upon which the magnets revolved, until the poles of both the stationary and revolving magnets became parallel with each other. At this point, the conducting wires from the battery changed their position by the motion of the shaft; the polarity of the stationary magnets was reversed, and, being now north poles, repelled the poles of the revolving magnets that they had before attracted, thus producing a constant revolution of the wheel.

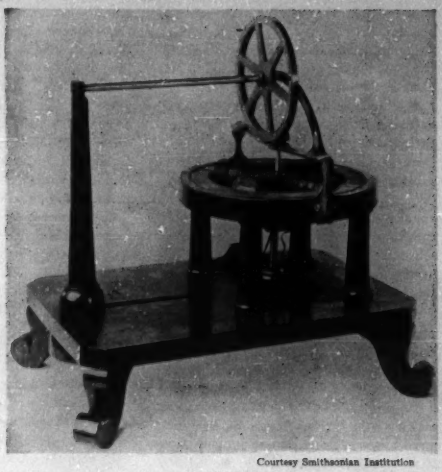
How Davenport actually reversed the flow of current through the electromagnets, his memoirs fail to state, but it probably consisted of some sort of a cam mechanism that did not operate very satisfactorily. But the satisfactory operation of his creation was of lesser importance than his successful attempts to convince others of the possibilities of this newly found aid to man. He had more followers who believed that he was developing a perpetual motion machine rather than a form of magnetic power. As the power developed by this early motor was only about 1/50th of a horsepower, those who viewed it, called it "mosquito power" and could see no practical value for such a device. His strong desire to establish his electric motor resulted in a neglect of his trade, a loss of customers, and near financial ruin. He sought encouragement from his village pastor who rebuked him with the statement, "If this wonderful power was good for anything, it would have been in use long ago." But undaunted by these setbacks, he took his motor to Middlebury College, there to seek the advice of the possessors of higher learning.

Professors Turner and Fowler of the Middlebury faculty watched a demonstration of the motor, recognized its potentialities, and advised Davenport to apply for a patent immediately. Elated, Davenport returned home and set about developing a new mechanism for reversing the flow of current through the electromagnets. In April 1835 he constructed a reversing mechanism that consisted of insulated segments rubbed by elastic flattened wires connected to the rotating armature. It was the inception of the modern commutator and it greatly improved the operation of Davenport's early motor. So enthusiastic was the press with this improvement that the August 13, 1835, issue of the Troy *Daily Budget* praised the accomplishments of Davenport and predicted that his name "would follow that of Henry's to the ends of the earth."

With continued encouragement from Professor Turner of Middlebury College, Professor Amos Eaton of Rensselaer Institute, Professor Joseph Henry of Princeton University, Professor Silliman of Yale University, and Professor Alexander Bache of Pennsylvania University, Davenport finally prepared a patent model and papers and presented them to the Patent Office. But misfortune followed him to Washington too, for the Patent Office burned and his model together with the papers was destroyed. However, with the aid of Ransom Cook, a prosperous manufacturer, another model was built and the papers rewritten. A photograph of this model, now on display at the Smithsonian Institution in Washington, D. C., is shown in Figure 2. He filed his claim again on January 24, 1837, and only 30 days later, on February 25,

Courtesy Smithsonian Institution

Figure 1 (left). Thomas Davenport, inventor of the electric motor



Courtesy Smithsonian Institution

Figure 2. Patent Office model of the Davenport motor

1837, received United States Patent number 132, a reproduction of which is shown in Figure 3. Here, without question, is evidence that Thomas Davenport is the inventor of the electric motor, about which the New York *Herald*, on April 27, 1837, so pompously stated:

> A Revolution in Philosophy—Dawn of a New Civilization . . . most extraordinary discovery, probably the greatest of ancient and modern times—the greatest the world has ever seen, the greatest the world will ever see.

The electric motor was not the only "first" for Thomas Davenport, for he built a model of the first electric railway, and, on the first electrically-driven printing press, which he built in 1839, he printed his own publication—"The Electro-Magnet and Mechanics' Intelligencer," the first American periodical on electricity. But, like the electric motor, they offered him no reward, and, in 1851, Thomas Davenport died penniless, with a broken heart, the patent on his electric motor having expired a few months prior to his death.

The De Jacobi Motor

On December 1, 1834, a Russian scientist, Moritz-Hermann De Jacobi presented a paper before the Academy of Sciences in Paris in which he stated that he had obtained rotation by electromagnetic methods in May 1834. Since Davenport wrote in his memoirs that he first succeeded in producing motion electrically in July 1834, the question of actual priority in the point of time, is a close one. If the results obtained by De Jacobi in May 1834 were effected by the same apparatus that he described in his paper before the Academy in December 1834, then the priority must be conceded to him, but there is no proof available to show that such is the case. However there is no doubt that the discoveries made by the two men were wholly independent.

In the year 1839, the Emperor Nicholas of Russia granted a sum of $12,000 to De Jacobi to enable him to prove that his electric motor had practical application. De Jacobi had a boat constructed, 28 feet long and 7 feet wide, which was propelled by means of paddles connected to an electric motor of his design. Figure 4 shows a model of the motor used on this craft. It consisted of two sets of electromagnets, eight in each set, one set being stationary and the other fastened to a rotatable wheel. Current from a bank of batteries was delivered to the rotating "star" through a commutator which reversed the polarity of the magnets eight times each revolution. The commutator used by De Jacobi appeared to be of better design than that used by Davenport and is very similar to the ones used today.

The De Jacobi boat, the first practical application of an electric motor, carried about 14 passengers and was powered by 320 Daniell cells, which is equivalent to about 100 of our present-day 6-volt storage batteries. In its trip up the Neva River it never achieved a speed greater than three miles an hour and consequently offered very little competition to man-propelled craft. The weight of the many batteries made the accomplishment impractical and it shortly was declared a failure. The cause of the failure was obvious, but the value of the electric motor as a new form of marine power was not forgotten.

The Page Motor

Charles Grafton Page, a physician from Salem, Mass., performed many electromagnetic experiments during the middle of the 19th century, the accounts of which may be found in Silliman's *American Journal of Science and Art*, the outstanding science publication of that time. In 1841 he became one of the two principal examiners in the United States Patent Office, and from 1844 to 1849 held the chair of chemistry at Columbian College, now George Washington University. In 1846 he built a small reciprocating electromagnetic engine which operated in an entirely different manner from that of Davenport's and De Jacobi's machines and, though the principle was sound, it never gained much popularity. Its motion resembled very much that of a steam engine's piston and in appearance was quite similar. It consisted of a hollow electromagnetic coil and an iron bar which was free to move within the coil. Both the coil and the bar were held vertically so that the bar was in a position of rest outside the coil when the coil was de-energized. Once current was allowed to flow through the coil, the bar would be attracted upward. Then when the current in the coil was interrupted, the bar would fall downward. This vertical motion was transferred to a flywheel by means of a crankshaft. A commutating

device, fastened to the crankshaft, automatically interrupted the flow of current to the coil at the proper time. In 1850, Page improved upon this single-acting motor by adding another solenoid which would pull the rod in the other direction and thus not depend upon the force of gravity for assistance. This improved model, for which Page was issued U.S. Patent number 10,480 dated January 31, 1854, is shown in Figure 5.

Although Page's motor did not receive public acclamation, it is worthy to note, as in the case of De Jacobi, that he made an outstanding contribution in publicizing the feasibility of such a device. In 1851, the Congress voted $50,000 to allow Page to construct an electric locomotive. A very interesting account of this early experiment may be found in the first American textbook on electric motors entitled "The Electric Motor and Its Applications," by Thomas C. Martin and Joseph Wetzler. Portions of this account as found on page 20 of this book are given here:

> Professor Page made a trial trip with his electromagnetic locomotive on Tuesday April 20, 1851, starting from Washington, along the tracks of the Washington and Baltimore Railroad. His locomotive was of 16 horsepower, employing 100 cells of Grove nitric acid battery, each having platinum plates 11 inches square. The progress of the locomotive was at first so slow that a boy was enabled to keep pace with it for several hundred feet. But the pace soon increased and Bladensburg, a distance of about five miles and a quarter, was reached, it is said, in 39 minutes. When, within two miles of that place, the locomotive began to run at a rate of 19 miles an hour, or seven miles faster than the greatest speed theretofore attained. This velocity was continued for a mile, when one of the cells cracked entirely open, and, as a consequence, the propelling power was partially weakened. Two of the other cells subsequently met with a similar disaster. It was found that the least jolt, such as caused by the end of a rail a little above the level, threw the batteries out of working order, and the result was a halt. This defect could not be overcome and Professor Page reluctantly abandoned his experiments in this special direction.

Page knew of Davenport's accomplishments in the field of electromagnetism and much correspondence passed between the two in which the merits of their individual devices were discussed. There were two major points of disagreement between them which time has shown that Davenport held the sounder theory. Page consistently adhered to the theory that the best results would be attained by simply discontinuing the flow of current to the electromagnets, without reversing the polarity. Davenport strongly persisted that the reversal should be performed. On the other point of argument, Page claimed that the size of the electric motor was definitely limited, since his experiments showed that the smaller machines were more efficient. Davenport could not see any sound foundation for such a claim and stated that such an idea affected him with "peculiar disagreeableness."

Conclusions

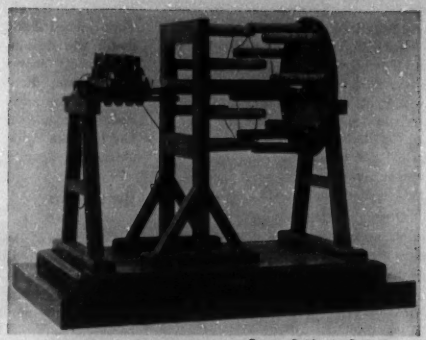
The efforts of these early pioneers, although seemingly failures, laid the necessary foundation for the development of the modern electric motor. Their accomplishments proved disappointing not because the soundness of the electric motor as a means of converting energy was in doubt, but rather because the device was born too soon. Since most crafts and skills were performed satisfactorily by hand at that time, the need of such labor-saving devices for mass production was not in demand. Also, the expense of the batteries to drive these primitive motors made its use prohibitive. Around the year 1850, Robert Hunt, an English natural philosopher, analyzed the relative cost of power obtained from a steam engine and from an electric motor using a battery and found that the electric power was 25 times more expensive than steam power. It did not occur to these early experimentors that the reverse process of electromagnetism was possible—that of the d-c generator. It was not until 1860 that Antonio Pacinotti, an Italian scientist, showed conclusively that by reversing the operation of the electric motor, "motion would produce electricity." Even after that discovery, industry was slow to make use of this knowledge. It was not until 1886 that the electric motor found its place in the American home when the Curtis, Crocker, Wheeler Company began manufacturing battery-operated motors for sewing machines. It was not until 1887 that the City of New York decided to lay the first tracks for its now famous "elevated." It was not until 1897 that the first electric automobile made its appearance. It was not until 1910 that the first electrically-driven washing machine was introduced to the American housewife. And it was not until 1915 that the *U.S.S. Mexico*, the first electrically propelled battleship, was launched.

Nevertheless, much credit should be given to those men who had the vision to foresee the marvels of their bundles of copper and steel. First to Thomas Davenport, the inventor of the electric motor; second, to Moritz-Hermann De Jacobi, who was the first to apply the electric motor practically to marine propulsion; and finally, to Charles Grafton Page, who was probably the first to apply successfully the electric motor to rail locomotion. Without a doubt, the most outstanding of these men is the Brandon blacksmith. In spite of the paucity of both his educational and financial advantages, he achieved a goal which others, far more fortunate than he, never had gained. His education could not have exceeded any more than three years of study based on today's standards and he was not gifted by any grant of money for the continuation of his experimental studies. Both De Jacobi and Page were learned men, fully equipped to battle the problems of their newly found science—electromotion. Moreover, their struggles were cushioned heavily by large governmental grants. It required supreme effort for a man, completely lacking such aids, to follow through to the time of his death an ambition that sprang from mere curiosity and resulted in one of man's most useful devices. It is hardly necessary to point out what fortunes would have been his had he renewed his patent rights and had he lived a few decades longer. The claims of his patent were extremely broad:

> Applying magnetic and electromagnetic power as a moving principle for machinery in the manner above described, or in any other substantially the same in principle.

What with an estimated 50,000,000 motors manufactured in the United States during the year 1947, a possessor of such a claim easily could mass a fortune far exceeding that of Rockefeller and Carnegie combined. But neither fame nor fortune was the lot of Thomas Davenport, a man who rightly deserved both.

In 1891 Franklin Leonard Pope, a charter member of the AIEE who became its president in 1884, wrote a series of five articles for *The Electrical Engineer* entitled: "Inventors of the Electric Motor." Although the title carried the word "inventor" in the plural, he proved conclusively that Thomas Davenport alone was the inventor of the electric motor. His evidence can be considered to be most trustworthy since he had opportunity to converse with living witnesses and examine original documents. On February 24, 1891, exactly 54 years, less a day, after Davenport had received his patent on the electric motor, Pope read a paper before a meeting of the AIEE in which he sought "to rescue from impending oblivion the true story of the invention of the electric motor." But now, more than 110 years since United States Patent number 132 was issued, the dark-

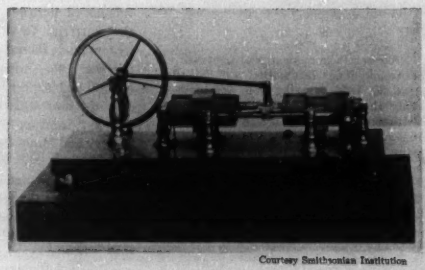


Courtesy Smithsonian Institution

Figure 3 (left). Facsimile of Daveport's patent on the electric motor

Figure 4 (above). The De Jacobi electric motor

Figure 5. The Page electric motor



Courtesy Smithsonian Institution

UNITED STATES PATENT OFFICE.

THOS. DAVENPORT, OF BRANDON, VERMONT.

IMPROVEMENT IN PROPELLING MACHINERY BY MAGNETISM AND ELECTRO-MAGNETISM.

Specification forming part of Letters Patent No. 139, dated February 25, 1837

To all whom it may concern:

Be it known that I, THOMAS DAVENPORT, of the town of Brandon, in the county of Rutland, State of Vermont, have made a discovery, being an Application of Magnetism and Electro-Magnetism to Propelling Machinery, which is described as follows, reference being had to the annexed drawings of the same, making part of this specification.

The machine for applying the power of magnetism and electro magnetism is described as follows:

The frame A may be made of a circular or any other figure, divided into two or more platforms, B and C, upon which the apparatus rests, of a size and strength adapted for the purpose intended.

The galvanic battery D is constructed by placing plates of copper and zinc E and F, alternately of any figure, in a vessel of diluted acid, G. From each vessel are two conductors, H and I, one from the copper and one from the zinc, leading to and in contact with copper plates K and L placed upon the lower platform. These plates or conductors are made in the form of a segment of a circle corresponding in number with the artificial magnets hereinafter described, placed around the shaft detached from one another and from the shaft, having a conductor leading from the copper plate of the battery to one of said plates on the lower platform, and another conductor leading from the zinc plate of the battery to the next plate on said lower platform, and so on alternately (if there be more than two plates on said lower platform) around the circle.

The galvanic magnets M N O P are constructed of arms or pieces of soft iron in the shape of a straight bar, horseshoe, or any other figure, wound with copper wire Q first insulated with silk between the coils. These arms project on lines from the center of a vertical shaft, R, turning on a pivot or point in the lower platform, said copper wires Q extending from the arms parallel, or nearly so, with the shaft, down to the copper plates K and L and in contact with them. The galvanic magnets are fixed on a horizontal wheel of wood, V, attached to the shaft.

The artificial magnets S T are made of steel and in the usual manner. They may be of any number and degree of strength and fixed on the upper platform, being segments of nearly the same circle as this platform; or, if galvanic magnets are used, (which may be done,) they may be made in the form of a crescent or horseshoe, with their poles pointing to the shaft. Having arranged these artificial magnets on the top of the upper circular platform, there will be a corresponding number of magnetic poles, the north marked 5 and the south pole 6. Now, we will suppose the machine to be in a quiescent state. The galvanic magnet No. 1 being opposite the north pole of the artificial magnets, the galvanic magnet No. 3 will, of course, be opposite the south pole No. 6, and the galvanic magnets Nos. 2 and 4 will be opposite each other, between the poles just mentioned. There being a corresponding number of copper plates or conductors placed below the artificial magnets around the shaft, but detached from it as well as from each other, with wires leading from the galvanic magnets to these plates and in contact with them, as before described, these wires will stand in the same position in relation to the copper plates that the galvanic magnets stand to the artificial magnets, but in contact with the plates.

Now, in order to put the machine in motion, the galvanic magnet No. 2, being changed by the galvanic current passing from the copper plate of the battery along the conductors and wires, becomes a north pole, while at the same time the magnet No. 4 is changed by the galvanic current passing from the zinc plate of the battery, and becomes a south pole. Of course the south pole of the artificial magnet No. 6 will attract the north pole of the galvanic magnet No. 2 and will move it a quarter of a circle. The south pole of the galvanic magnet No. 4, being at the same time attracted by the north pole No. 5, and the south magnet No. 4 also to perform a quarter of a circle. The momentum of the galvanic arms will carry them past the centers of the poles Nos. 5 and 6, at which time the several wires from the galvanic magnets will have changed their positions in relation to the copper plates or conductors. For instance, the north pole No. 2 having now become a south pole by reason of its wire being brought in contact with the conductors of the zinc plate, and No. 4 having in like manner become a north pole, its wire having changed its position from the zinc plate to the copper plate, the poles of the galvanic magnets are, of course, now repelled by the poles that below attracted them, and in this manner the operation is continued, producing a rotary motion in the shaft, which motion is conveyed to machinery for the purpose of propelling the same.

The discovery here claimed, and desired to be secured by Letters Patent, consists in—

Applying magnetic and electro magnetic power as a moving principle for machinery in the manner above described, or in any other substantially the same in principle.

THOMAS DAVENPORT

Witnesses:
W. W. AYARS,
CHAS. A. CIOS

FACSIMILE OF THE ORIGINAL PATENT

ness of that oblivion seems to have engulfed the accomplishments of this early American inventor. It would seem fitting that, at this time, some lasting tribute be made to perpetuate the memory of Thomas Davenport. Throughout the history of our electrical science fitting honor has been bestowed upon such men as Alessandro Volta, Andre-Marie Ampere, Georg Simon Ohm, James Watt, Joseph Henry, Michael Faraday—such that their names are of common usage in the classroom and in industry. Would it not seem appropriate that Thomas Davenport be honored in a similar manner? Could not the unit of performance of rotating engines, the horsepower, which is used so much in connection with electric motors, be changed to "davens," in honor of the inventor? What procedure must be followed to effect such a change, I do not know, but an expression of our tribute to this almost forgotten American seems to be wanting, and the opportunity to change this archaic term may present the occasion to bestow this belated honor.

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No Discussion

Facsimile Transmission for Pickup and Delivery of Telegrams

G. H. RIDINGS
NONMEMBER AIEE

As the tempo of progress in science, industry, transportation—in every field of human endeavor—increases with each passing day, so, too, does the demand for swifter, less expensive, and more convenient methods of record communication, for it is usually by record communication that the various steps of any important project are undertaken. Business men and banks rely upon some form of record communication for the execution of orders, contracts, loans, for expediting sales and deliveries, for the presentation of statements, in fact, for nearly every business transaction. So, too, do most organizations in transportation and allied fields, news gathering and law enforcement agencies, and the various agencies of government. It is essential that progress in record communications keep pace with that in other fields.

We are keenly aware of the tremendous progress made in the field of transportation, from the early days of the stagecoach and covered wagon. Although somewhat less spectacular, the extent of progress in record communications over the same period of time has not been less notable. From the days of the key and sounder, with its many manual retransmissions, and relatively few miles of pole lines subject to the whims of the elements, the industry has progressed through the inventions of the duplex, the printing telegraph, and the multiplex to the vast carrier system networks of today, with the varioplex, and push-button and automatic switching to provide the utmost in swift, efficient service. The necessity for manual retransmission is greatly reduced and well on the way to being eliminated. The number of communication channels has been increased manyfold and circuit interruptions minimized. The radio-beam networks now being installed are still further increasing the number and quality of communication channels and should go a long way towards eliminating circuit interruptions due to weather and atmospheric conditions.

While great strides have been made in recent years in intercity handling of telegrams, the problems of intracity, or terminal handling, have not been overlooked. Business organizations have been provided with teleprinters having features especially designed for their particular needs including direct connection with a Western Union central office. Where the volume of business is both large and concentrated, direct-wire connections are provided; with lesser volume of a concentrated nature, telemeter service provides an efficient, direct, service facility. Both services include teleprinters. The newest development in this field is the "Telefax" employing facsimile principles. It eliminates keyboard transmission, and since it is essentially a reproductive process, the opportunity for error is removed. Telefax has already received the thorough test of in-plant and customer use. Circuit concentrators were developed to meet Western Union central-office requirements. Customer enthusiasm has encouraged the company to embark upon a program of expanding this type of patron connection to include business offices, hotels, transportation terminals, apartment houses, and similar locations.

Terminal Handling for the Small Account Customer

Where terminal handling facilities of the type already described can be made available, the time and cost incident to messenger pickup and delivery are eliminated. However, volume and equipment costs are all important considerations in substituting mechanical for physical terminal handling methods. Today the simplest teleprinter represents a sizeable investment which, together with operating and maintenance charges, make it uneconomical to employ such equipment where telegraph volume is comparatively light. While the cost of Telefax equipment was lower, and it afforded more reasonable operating and maintenance charges, the initial cost of such equipment was still too high to permit of widespread use in the moderate volume field. Obviously, it became essential to lick this problem of economics if we were to capture the sales and service value of direct-service methods for the user with moderate volume. This terminal handling problem assumed even more importance as Western Union mechanized its internal operations and eliminated manual retransmission in a relaying process. Too often, the service advantage thus accomplished was in great measure offset by the delay inherent in messenger delivery.

What, then, were the steps to be taken to make it economically practical to provide an efficient pickup and delivery medium for users whose volume requirements did not justify expensive teleprinter equipment? In the order of importance they are:

1. To develop some form of pickup and delivery device whose initial cost and operating and maintenance charges are so low that it may be economically employed to provide a direct-wire connection to the nearest Western Union central office for the many thousands of small businesses whose use of the telegraph is not sufficiently frequent to justify expensive printer terminal equipment.

2. To develop automatic or semiautomatic pickup and delivery devices which may be installed in every large office building, apartment house, and hotel, whereby tenants

Paper 48-233, recommended by the AIEE communication committee and approved by the AIEE technical program committee for presentation at the AIEE Middle Eastern District meeting, Washington, D. C., October 5-7, 1948. Manuscript submitted February 16, 1948; made available for printing September 10, 1948.

G. H. Ridings is assistant Telefax research engineer with the Western Union Telegraph Company, New York, N. Y.

Figure 1. Telefax transmitter-recorder with optical scanning

could secure most of the benefits of a direct connection to the Western Union central office, even though their individual use of the telegraph might be infrequent. These installations would be, in effect, miniature Western Union branch offices.

3. To provide, for residential areas, automatic, conveniently located, pickup devices and Telecars for delivery. These Telecars are automobiles equipped with Telefax recorders having direct, radio connection to the Western Union central office.

It should be noted that all three phases of this program require the development of simple, inexpensive, easy to operate, automatic or semiautomatic machines. In addition, these machines must be sturdy and require a minimum of maintenance. No process which requires that the intelligence be transcribed into a code for transmission could be employed in the design of equipment sufficiently automatic for the purposes outlined before. And, of course, no process involving the transcribing of the human voice could be considered sufficiently safe because there would be no opportunity, with mechanical recording, to verify imperfections. The requirements can only be fulfilled by employing some sort of facsimile process wherein the telegram is treated as a unit rather than as a series of units.

What Has Already Been Accomplished

Progress has already been made towards the goal previously outlined. The basic ground work has been laid and future progress should be at a rapid rate. For one thing, a record sheet called "Teledeltos" has been developed by Western Union and its use under widely varying conditions, both in telegraph service and in equipment employed by our armed forces during the war, has shown it to be entirely satisfactory for these purposes.

"Teledeltos" is a dry recording paper which, unlike most recording media, requires no processing of any kind before or after recording. The coating of this conducting paper is light in color and turns black when electric current of about 20 milliamperes passes through it. Either alternating or direct current may be employed in recording. The paper is sensitive only to electrical impulses and is affected by light or moisture much less than is ordinary writing paper. It produces a clear-cut and permanent record with no "fixing" of any kind required. There is no apparent ageing of records many years old. Teledeltos is extremely fast, pulses of 0.0001 second duration being easily recorded. It is capable of reproduction speeds many times the highest yet employed in commercial, facsimile equipment. Its current density characteristic is such that fairly good half-tone reproduction may be obtained if desired, without special circuit arrangements. Its cost is a small fraction of the cost of photographic paper. Only the simplest of recording equipment is needed. Amplified tone signals, without rectification, may be applied directly to the paper by means of a stylus riding continuously on its surface.

A number of different types of automatic and semiautomatic transmitters, transmitter-recorders and recorders have been developed and employed commercially on a limited scale for the pickup and delivery of telegrams. Among these is the transmitter-recorder shown in Figure 1, designed for use in a patron's office. It is about the size and shape of a teleprinter or typewriter. This machine may be placed on a table or desk or may be furnished complete with a pedestal. The machine is 11 inches high, $16^1/_2$ inches wide, and $15^1/_2$ inches deep. It is self-contained, requiring only the line pair, a ground, and a source of 110-volt 50-to-60-cycle alternating current for its operation. This machine has three controls: a send-receive switch, a vernier control for adjusting the density of the recorded copy; and a starting switch. To transmit a telegram, it is wrapped by hand around the drum which protrudes from one side of the cover. The send-receive switch is set to "send" and the starting switch operated. Thereafter, operation is entirely automatic. A call is set up in a concentrator at the central office. There, a switching unit in which the patron's lines terminate automatically connects an idle recorder to the circuit and the message is recorded. After the message has been completely scanned, the patron's machine shuts down automatically.

Figure 2. Automatic Telefax transmitter

To transmit a telegram from the central office to a patron, the attendant wraps the message about a drum and inserts it into one of several transmitters which are a part of the concentrator. The operator then connects the transmitter to the patron's line, causing a buzzer to sound summoning him to his machine. The patron places a sheet of Teledeltos on the drum, sets the send-receive switch to "receive," and operates the "start" switch. Transmission starts immediately and automatically. When the message has been recorded and the transmitter stopped, a red signal lamp on the top of the patron's machine will light and the patron may stop the machine and remove the telegram. If the patron does not stop the machine it will continue to scan until the entire blank has been scanned, at which time it will shut itself off automatically. If, in sending or receiving a message, the send-receive switch is inadvertently placed or left in the wrong position, the red signal lamp will light, and transmission will not start until the switch is operated correctly.

Another development is the transmitter shown in Figure 2 designed to facilitate the pickup of telegrams in office buildings,

apartment houses, hotels, and similar locations. In this equipment we have taken another step towards the goal of a completely automatic telegraph system. With these machines it is as easy to file a telegram as it is to mail a letter. The customer or agent simply presses a button and drops the telegram into the slot in the front of the machine. This is all that is required of him. The telegram automatically wraps itself about the drum of the transmitter and the entrance to the slot closes, preventing the insertion of another telegram until the first one has been transmitted. Several of these machines may be operated over one line pair where the volume of business is light. Circuits from these transmitters terminate in a central office in a facsimile receiving concentrator, from which all of the functions of the transmitter are controlled. After the message has been recorded at the central office, the telegram is stripped from the drum of the transmitter and deposited into a receptacle beneath the drum, and the transmitter released.

Two types of transmitters are employed in this service. One is a wall model about 30 inches high, 15 inches wide, and 8 inches deep. The other, illustrated in Figure 2, is a table model, 24 inches high, 16 inches wide, and 12 inches deep. This model may be equipped with a pedestal, if desired, so that it will rest directly on the floor. Both machines are completely self-contained requiring only a line pair, a ground, and a source of 110-volt 50-60 cycle alternating current for their operation. Where an agent is employed to operate it, the machine is equipped with a push-button for starting. Where it is to be installed for unattended service, in an office building lobby for example, it may be equipped with a lock switch and charge account patrons in the building supplied with keys. In the case of the unattended installation in a public place, a coin-slot mechanism may be employed.

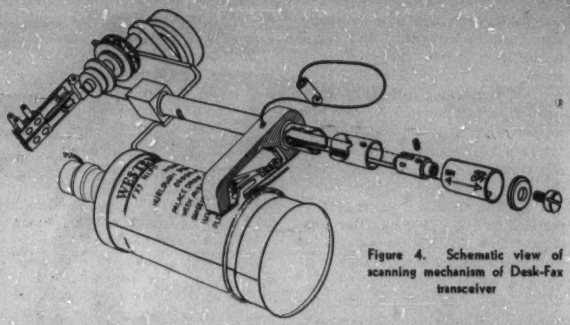

Figure 4. Schematic view of scanning mechanism of Desk-Fax transceiver

Figure 3 illustrates an automatic, Telefax recorder employed in the Telecar delivery service referred to previously. This recorder, which is controlled by radio from the central office, employs a roll of Teledeltos, from which a length sufficient for one message is automatically formed into a cylinder and scanned internally, then cut from the roll and ejected from the front of the recorder. The process is automatic, and while the operator of the car is delivering one telegram, another is being recorded and deposited into the convenient receptacle just under the dash of the car. Radio and control equipment are located in the trunk of the car.

Radical Changes in Design Necessary to Lower Equipment Cost

When the patron's transmitter-recorder shown in Figure 1, was developed, it was expected that it would answer the service requirements of a large majority of small businesses. The results of the limited installations which have been in service through the war years have been highly satisfactory. However, post-war economic conditions have made it necessary to develop a much simpler and cheaper machine to serve a larger portion of the Telegraph Company's patrons, particularly those in the small business category, and thus make possible nationwide expansion of patron Telefax service.

To undertake such a development, it was necessary to think along radically different lines than heretofore. Conventional methods of scanning, line-feed, and so forth, had to be discarded. The simplest means of facsimile recording is that wherein the facsimile signals are applied directly to a stylus riding continuously on the surface of an electrosensitive record sheet, and having such a record sheet in Teledeltos, this method of recording was chosen for our transceiver. Now, it was reasoned, that if some form of conductive pickup could be employed, this same stylus could be utilized to scan

Figure 3. Telefax recorder for mobile delivery of telegrams

Figure 5. Desk-Fax transceiver—cover removed

the subject copy and a really simple transceiver would result. Why not use conductive pickup? The principle is sound, as old as facsimile itself. True, good half-tone characteristic is impossible, but for message transmission it is not necessary.

What about the preparation of subject copy? The use of conductive pickup simply means furnishing patrons with a different kind of blank than would be provided for use with some other type of transmitter, since it is accepted practice to provide the customer with blanks upon which to prepare his telegrams.

The problem then resolved itself, for the most part, into the development of a simple, economical, easy-to-use sending blank upon which writing and typing would have a different conductivity from the blank itself. Two methods for the preparation of subject copy have been developed which give satisfactory results. One method employs a sheet of Teledeltos, similar to that which is used as the record sheet. Over this is placed a sheet of special carbon paper, and writing or typing on the back of the carbon paper transfers the electrically conducting carbon onto the surface of the Teledeltos. The carbon penetrates the thin insulating coating of the Teledeltos, making contact with the conductive base of the paper. A stylus scanning such a subject copy would traverse characters of low resistance whereas the background of the paper would present an extremely high resistance—practically open circuit.

The second method employs a sheet of black conducting base stock which is ordinarily coated to form Teledeltos. This method also involves the use of a special carbon paper. In this case the so-called carbon paper is coated with a white insulating wax so that writing or typing on its reverse side transfers white insulating characters to the black conducting paper. A stylus traversing this subject copy would encounter characters of very high resistance—practically open circuit—whereas the background of the paper would present a fairly low resistance. Having such a subject copy, it is a simple matter to use it to key or modulate the output of a vacuum-tube oscillator. With the aid of a bridge arrangement it is equally easy to provide for signal conversion so that with a simple adjustment a "positive" or a "negative" may be transmitted from either of the two types of subject copy.

Mechanically, also, our transceiver must be made as simple as possible. The simplest scanning mechanism consists of a drum rotated at constant speed upon which is mounted the subject copy or record sheet, while the scanning point or stylus is caused to move at a uniform rate along the length of the drum. Of course, the same relative motion could be secured by causing the drum to move along its axis as it rotates, with the scanning point remaining fixed in space, but the former method is employed in this transceiver. Conventionally, this relative motion, or line feed, is secured by means of a feed-screw and half-nut, but, in our transceiver, it is accomplished by winding up a cord on a drum or reel powered by a small clock motor. The reel is frictionally coupled to the motor shaft so that the stylus housing may be manually returned to the "start" position after the completion of each transmission. A slotted, tubular track is provided to support and guide the stylus housing, with the slot fashioned in such a manner as to cause the stylus housing to be raised from the drum in the "start" position to facilitate loading. Figure 4 shows schematically how this is accomplished. The cord which is attached to the stylus housing at the pin projecting through the slot in the tube passes over the pulley at the right end of the tube and then back again through the tube to the reel on the clock motor shaft at the left of the machine. The motor causes the stylus housing to feed to the right, and it is returned manually by pushing it towards the left at the end of transmission. The pin riding up the incline in the tube slot at the extreme left lifts the stylus housing to a vertical position. The stylus holder is a replaceable unit to facilitate renewal, and the stylus itself is pivoted so that when the stylus housing is in an upright position the stylus is completely withdrawn into the housing. Pivoting the stylus also prevents damage to this vital part of the mechanism if the patron should improperly remove a blank from the drum without first returning the housing to the "start" (upright) position.

Mechanism of the New Transceiver

Figure 5 is a close-up of the mechanism of the transceiver. The subject copy or record sheet is held on the drum by the flange at its left end and by a toroidal spring which is rolled onto it from the right end. A small, synchronous motor mounted on the rear of an L-shaped bracket drives the drum through a single set of gears, and a simple, fabric brake on the drum shaft takes up any back-lash. On the drum shaft is a commutator which is used for phasing. Above the drum motor, on top of the bracket, is the small clock motor with frictionally coupled reel which causes the stylus housing to move out along the tube parallel to the drum. At the extreme right end of this tube is a sleeve which is connected by means of a rod extending through the tube back to a "start-stop" switch mounted on top of the bracket. This sleeve which is operated manually to the left to start the transceiver will be moved to the right or "off" position by the motion of the stylus housing as it completes the scanning of the message blank. This mechanism is shown diagrammatically in Figure 4.

The controls of this transceiver are simpler than those of the larger machine of Figure 1. Both machines employ a "start-stop" switch which serves also as an end-of-message switch, but this newer transceiver does not have a send-receive switch nor a control to regulate the density of the recorded copy. In this transceiver, operation of the "start" switch automatically sets up the machine as a transmitter unless a call has been placed at the central office causing the buzzer to operate in the patron's machine. Under these conditions, operation of the "start" switch automatically sets up the transceiver as a recorder.

Since the circuits from these transceivers may terminate in large concentrators at the central office, the use of number sheets there would be extremely cumbersome. Provision is therefore made for the patron to acknowledge individually the receipt of each telegram. This is accomplished by means of a small neon lamp (which, together with the buzzer, indicates to the patron the completion of a transmission to him), and a push-button which he operates after reading the received message.

The mechanism is assembled from simple stamped and bent sheet metal parts and die castings wherever possible, with a minimum of machined parts, most of which are of the screw-machine variety. The drum and stylus-feed motors, gears, "start-stop" switch and stylus connector are inexpensive commercial items. There are no close tolerances, and the stylus construction is such that considerable eccentricity of the drum may be tolerated. The entire mechanism mounts on the L-shaped bracket and may be assembled or disassembled in a very few minutes with no special tools. There are few adjustments, none critical.

Electronics and Control Circuits—Characteristics

The mechanism mounts across the front of a sheet-metal chassis which contains the electronics and control circuits.

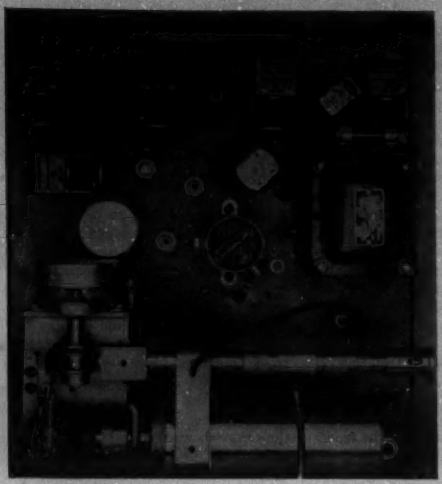

Figure 6. Top view of Desk-Fax transceiver, showing layout of parts

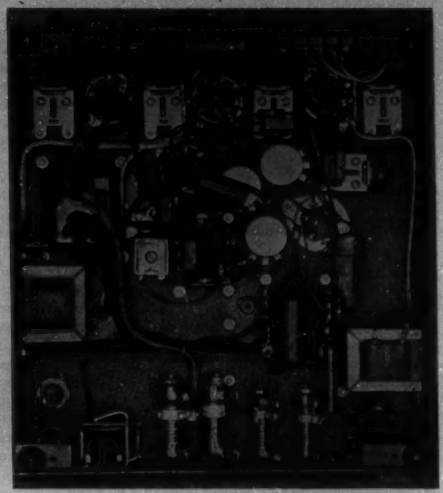

Figure 7. Bottom view of Desk-Fax transceiver, showing accessibility of components

Wiring of the mechanism terminates on convenient terminal strips on the chassis to facilitate servicing or replacement. Figure 6 shows the complete unit with cover removed and Figure 7 shows the under side of the chassis with bottom plate removed. Although the chassis measures only $10^1/_8$ by $11^1/_8$ inches, there is no crowding of parts, and relays, vacuum tube sockets, terminal strips, and other vital components are readily accessible for test or maintenance purposes. The electronic circuits employ four tubes, a rectifier for power supply, a dual triode which serves as oscillator and transmitting amplifier, and a high-mu pentode and a power output tube which serve as the recording amplifier. The use of separate tubes for transmitting and recording would appear an extravagance, but such is not the case. By switching tubes from one circuit to the other, at most, one tube would be saved the cost of which is more than outweighed by the simplification of switching and wiring which the use of separate tubes permits.

Controls are provided for transmitting and recording levels and for bridge balancing. These are set for each installation and remain unchanged unless servicing is required. A terminal strip is provided for attaching line wires and ground, another for attaching a rubber-covered power cord so that the desired length of cord for each installation may be provided. With the exception of the oscillator coil, all components of the electronic and control circuits are inexpensive, commercial items. The power transformer is similar to that used in the smallest a-c table-model radios. A transformer couples the amplifier to the line pair, serving as an output transformer when transmitting and as an input transformer when recording. Control relays are the inexpensive, rugged type employed in juke boxes and pinball machines. No critical adjustments are required. The relays are mounted so that the contacts are accessible for cleaning. Wiring of the unit is comparable to that of a small table-model radio and can be done in economical assembly-line fashion. The over-all dimensions of the transceiver, with cover and blank holder, are $11^1/_8$ inches by $11^1/_4$ inches by 7 inches. It weighs about 19 pounds. Maximum power consumption is 100 watts, and no power is consumed when the machine is idle.

The oscillator frequency is about 1,900 cycles and a total band width of 1,900 cycles is adequate so that the transceiver will operate over an ordinary telephone pair. The 2-inch drum rotates at 180 rpm and the line feed is 125 lines per inch to match the index of cooperation of the central office equipment which employs a larger size blank. Blank size is $6^1/_2$ inches by $4^1/_2$ inches with a useful message area of $5^3/_8$ inches by 3 inches. This will accommodate about 150 typewritten words and is transmitted in two minutes. No special provisions for synchronization are made, the synchronous driving motor being operated from the same commercial 50 or 60 cycle a-c power source employed at the central office. Phasing, signalling, and other necessary control functions operate on a d-c basis using the physical wires with ground return through a center-tapped primary of the line coupling transformer. Phasing clutches on the central office transmitters and recorders are controlled by the commutator of the patron's machine.

Central-Office Equipment—Circuit Operation

Circuits from these transceivers terminate in Western Union central offices in concentrators made up in multiples of 50-patron units. Each 50-patron unit is provided with four conventional telegram-size optical transmitters and six conventional-size page-type continuous recorders. The transmitters employ the so-called color-sensitive photocell and improved electronics so that messages received by tape printer, page printer, and various other means may be transmitted with equal fidelity.

To send a message to a patron, the central-office operator wraps the message about a drum, inserts the drum into a transmitter and plugs up to that patron's circuit. This removes positive, standby

Figure 8. Desk-Fax transceiver with stylus scanning for transmitting and recording

potential and applies negative potential to the line over the simplex circuit which operates a relay in the patron's transceiver, causing the buzzer to sound. When the patron answers the call by loading his drum with a Teledeltos recording blank and operating the "start" switch, the buzzer stops and the transceiver is automatically set up as a recorder. As soon as the transceiver's tubes heat up, a relay operates to start the stylus-feed motor and to send out phasing pulses (interruption of the direct current over the simplex loop). The central-office transmitter phases immediately and the transmission of the message proceeds. When the message has been transmitted, the transmitter stops and polarity of the battery on the line is reversed causing the acknowledge lamp and buzzer to operate in the patron's transceiver. After the patron has examined the recorded message he operates the acknowledge push-button which extinguishes the light, stops the buzzer and opens the simplex loop giving an acknowledgment indication to the central-office operator. The operator then removes the transmitter from the line.

To send a message to the central office, the patron wraps the subject copy, prepared as described previously, about the drum and operates the start switch. This sets up the transceiver as a transmitter and operates a relay at the central office which gives appropriate indication to the operator that a call from that patron is waiting, who then connects a recorder to the calling circuit and operates a start button. As soon as the heat relay of the transceiver has operated, the recorder phases from the first phasing pulse from the transceiver's commutator. Contacts on the phasing relay reverse the potential on the line which starts the stylus-feed motor at the transceiver. Transmission continues until the transceiver shuts down automatically or until the patron manually operates the "start-stop" switch. The recorder stops also, and a light indicates to the operator that the message is complete, who then rotates a paper-feed-out knob on the recorder until the conventional telegram-blank length of paper is fed out, at which time the light is extinguished. The message is then torn off and the recorder disconnected.

The first concentrators for this service will be of the plug-and-jack manual type, the switching turret resembling the familiar PBX switchboard. Circuits have been worked out for a more automatic concentrator with automatic line finders on the receiving side and push-button switching on the sending side, should this method of operation be found desirable. At the present time there is some question as to the economy of such operation since a certain amount of manual attention is necessary in any event for checking, acknowledging, and other functions involved in transacting business directly with the patron.

A New Patron-to-Patron Transceiver

A machine similar to this transceiver has been developed for patron-to-patron use. That is, two machines work together directly without any central office concentrator equipment. The same mechanism is employed with some rearrangement of the control circuits and a simple phasing circuit whereby one motor drifts into step with the other. Phasing is accomplished without the use of conventional magnets or clutches, employing the commutator on each motor shaft and an added capacitor which is connected across the recorder driving motor, causing it to run slightly under synchronous speed until pulses from the two commutators are in step, at which time the capacitor is removed and both motors run synchronously. Such machines may be used to provide intercommunication for business organizations or for pick-up and delivery of telegrams where only a few patrons are involved and a concentrator is not justified.

Installations of the type just described have been in operation in one of our eastern cities for the past several months from which considerable information has been obtained that has been helpful in improving the design. A number of ways were found to simplify and improve the mechanism, and the mechanical and electronic portions which were originally separate units are now combined into one, permitting a sizeable reduction in manufacturing costs. The trial installations have shown this method to be quite satisfactory for pick-up and delivery of telegrams. Reports are that patrons have found it to be so convenient that their use of the telegraph has substantially increased since they were provided with this facility.

Two thousand transceivers, incorporating all of the improvements resulting from these trial installations, are now being manufactured. The manufacturing cost of this unit is expected to be about one-third that of the larger patron's machine of Figure 1. Central-office concentrator equipment is being manufactured for installations in six major cities. Although over 1,000,000 telegrams a year are handled by facsimile equipment, these new installations will increase the volume of telegraph traffic handled by facsimile principles manyfold and should pave the way for still larger installations in the future, particularly for small volume telegraph users.

While this simple low-cost transceiver cannot be regarded as the ultimate in record communications any more than Ford's Model T was the ultimate in transportation, we believe that it will answer a corresponding need in an equally creditable manner.

No Discussion

An Automatic Standing-Wave Indicator

PHILIP J. ALLEN
NONMEMBER AIEE

WITH microwave standing-wave measurements in the laboratory becoming as commonplace as sewing in the home, it is not surprising that the hand-operated travelling probe should soon develop into a "sewing machine." This paper describes an X-band (3-centimeter) automatic standing-wave indicator in use at the Naval Research Laboratory which has cut to a fraction the time required to make standing-wave measurements, and not only reduces the tedium accompanying the usual routine, but actually adds interest to the task. The apparatus reproduces the standing-wave pattern on a cathode-ray tube, and measurements are made directly from calibrated scales. This type of presentation allows unusually rapid adjustment of radio-frequency components, since the effects of adjustments are apparent instantly. A number of novel techniques are thereby made possible.

Impedance measurements at microwave frequencies generally are made relative to some reference impedance, usually the characteristic impedance of the transmission line. The problem of determining impedance by means of standing-wave measurements involves the determining of two factors: the standing-wave ratio in the transmission line (conveniently the ratio of the maximum to minimum voltage amplitude); and the phase or position of the voltage minimum relative to a point of known impedance. This known impedance is most conveniently made zero by placing a short circuit at a known position in the transmission line. Conventionally, these measurements are made with a manually-operated, travelling probe which samples the r-f field along the line, and by means of a detector and auxiliary equipment, usually an amplifier and indicating meter, the ratio of voltage maximum to voltage minimum is obtained. The relative position of the voltage minimum is determined by converting the linear displacement of the probe into an equivalent angle, or wave-length fraction. When the standing-wave ratio and the relative position of a minimum of the standing-wave pattern have been determined, the desired impedance can be found, using transmission line equations, or some form of circle diagram or impedance chart. Thus, for rapid determinations of impedance, the values of standing-wave ratio and relative location of the voltage minimum must be readily obtainable.

In the event that it is sufficient to know only the magnitude of the reactance of a component being tested, the task is reduced to one of determining the standing-wave ratio alone. In still other instances, it is sufficient merely to adjust a component for minimum standing-wave ratio for best "match" to the transmission line, without actually making a quantitative determination. However, even this operation can become a task if numerous determinations have to be made, as is usual when a component is being adjusted for optimum match to the transmission line. In many such instances, the time required to determine the effect of an adjustment is frequently much greater than the time required for the adjustment itself, with a consequent inefficient use of time. Thus, a method which provides an instantaneous indication of the effect caused by adjustment can reduce over-all adjustment time considerably.

The automatic standing-wave indicator was developed with these several considerations in mind, and consists of a specially-designed reciprocating probe, a conventional high-gain audio amplifier, and a special indicator employing a long-persistence cathode-ray tube. In operation, a pictorial representation of the standing-wave pattern in the transmission line is reproduced on the cathode-ray tube, and from this picture, the voltage standing-wave ratio (VSWR) and relative phase of the standing-wave pattern may be quickly and accurately read from calibrated scales.

The r-f bench setup for use with the automatic standing-wave indicator differs little from the conventional arrangement. In place of the usual manually-operated, travelling probe, there is a motor-driven, reciprocating probe, and the usual indicating meter at the amplifier output is replaced by a calibrated cathode-ray indicator. A typical test setup employing the automatic standing-wave indicator and a minimum of microwave equipment is shown in Figure 1. The waveguide components include a *2K25* klystron signal source, attenuator pad, calibrated wavemeter, variable attenuator, reciprocating probe, and a crystal detector termination. Behind the wave-guide equipment, the high-gain amplifier rests on top of the klystron power supply and modulator. To the right, is the cathode-ray indicator used for reading both standing-wave ratio and phase of the pattern. As most of the components of the setup are conventional, only the special units of the automatic standing-wave indicator will be described.

The Reciprocating Probe

Basically, the reciprocating probe is similar to the conventional slotted-line probe having adjustable probe depth and matching facilities for the crystal detector. Although there is still provision for manual operation of the probe, a 100-rpm motor, flywheel and connecting rod have been added to make it travel automatically back and forth along the slotted line over a distance slightly greater than one guide wave length. As the probe moves, a pair of contact fingers mounted on an insulating block attached to the moving

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PHILIP J. ALLEN is with the Naval Research Laboratory, Anacostia, D. C.

probe wipe along two wire-wound linear resistor strips mounted below and parallel to the waveguide. These resistors are cross-connected and supplied with an adjustable d-c voltage. This arrangement, which may be seen in Figure 2, is used to generate a push-pull horizontal sweep voltage for the cathode-ray indicator. The lateral position of the spot on the indicator thus is made to correspond to the position of the probe along the transmission line. When the equipment is functioning without a vertical signal applied to the indicator, the spot on the cathode-ray tube moves back and forth with the reciprocating probe, drawing a straight horizontal base line. The output of the probe crystal detector is fed through the high-gain amplifier, and connecting the amplifier output to the vertical deflecting plates of the indicator results in a picture of the standing-wave pattern in the transmission line.

The Amplifier

A number of good high-gain amplifiers, some employing a parallel-T feed-back network, intended for standing-wave measurements are commercially available, and most of these can be adapted readily, with slight modification, for use with the automatic standing-wave indicator. These amplifiers are usually equipped with a built-in output meter and rectifier, but may not have provision for obtaining the necessary audio output. With the amplifier being used, the only modification necessary was the installation of an audio output jack, the available output voltage being more than adequate for the vertical deflection desired. To get maximum output voltage, the amplifier is operated with the interior–exterior meter switch in the exterior position. This reduces wear and tear on the output meter, since the amplifier is operated at a higher output level than when used normally with the internal meter.

The Indicator

It is desirable and important that the interpretation of the standing-wave indications be as simple and convenient as possible, and, at the same time, allowing maximum utility of the automatic standing-wave indicator. For this reason, the presentation is in the form of a visible standing-wave pattern, from which variations in standing-wave ratio and phase can be continuously observed. A long-persistence cathode-ray tube is used for the presentation since the horizontal sweep generated by the probe takes place only 200 times a minute. The vertical sweep frequency is that of the klystron modulator and may be several hundred cycles per second.

The indicator unit, shown in Figure 3, houses the high-voltage power supply for the cathode-ray tube and a gas-tube-regulated 300-volt d-c supply used for the bias on the horizontal sweep-generating resistors. A wire-wound potentiometer for adjusting horizontal sweep amplitude permits varying the voltage applied to these resistors from zero to the full 300 volts.

A rectangular lucite scale, about three inches wide and located in front of the cathode-ray tube, is calibrated with engraved horizontal lines which are used to indicate directly the VSWR of the normalized standing-wave pattern appearing on the cathode-ray tube. This scale shown in Figure 4, is mounted on a gear-driven rack controlled by the dial in the center of the indicator panel which allows the scale to be moved manually across the face of the indicator tube. The width of the scale and the size of the gear have been chosen so that in one revolution of the dial, the scale travels laterally across the face of the cathode-ray tube a distance equal to the scale width. The movable scale and dial are used for indicating the phase shift of the standing-wave pattern on the transmission line. The dial is calibrated in either degrees, radians, or decimals of wave length, depending on the application.

Although the a-c output of the amplifier produces a symmetrical wave form possessing a "modulation envelope" representing the standing-wave pattern, only the positive half of the pattern is used. An adjustable, vertical-centering voltage applied to the cathode-ray tube shifts the center of the symmetrical pattern to the position of the base line shown in the patterns of Figure 4, and "clipping" of the bottom half of the symmetrical waveform is accomplished with a strip of black masking tape stretched across the lower part of the cathode-ray tube face.

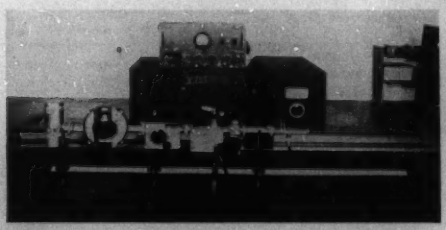
The motor on the reciprocating probe is controlled by a switch on the indicator panel which actuates also a relay to remove the spot from the cathode-ray tube to avoid burning the screen whenever the probe is not operating.

Operation

With standing waves existing in the transmission line, and the reciprocating probe operating, a standing-wave pattern similar to one of those shown in Figure 4 appears on the indicator. To read the VSWR it is necessary only to normalize the pattern by adjusting the variable r-f attenuator or the amplifier gain until the maxima of the standing-wave pattern are tangent to the top horizontal line on the scale. The VSWR is then read as the value associated with the horizontal line

Figure 1. (below) Typical setup employing automatic standing-wave indicator

Figure 2. (right) Rear of reciprocating probe showing sweep-generating resistors



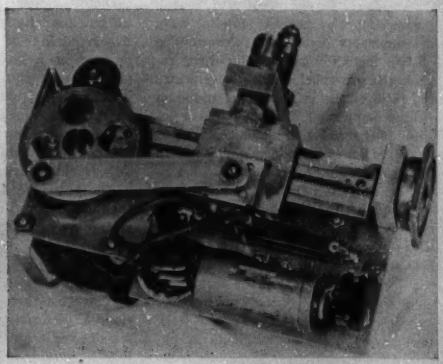


Figure 3. Standing-wave indicator

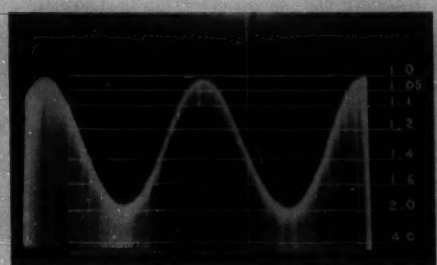
passing tangent to the minima of the pattern. Slight visual interpolation may be necessary.

When determinations of the relative phase of the standing-wave pattern along the transmission line are to be made, the horizontal sweep amplitude is adjusted until one edge of the movable scale and the vertical center line coincide with adjacent minima of the standing-wave pattern. This adjustment can be made most accurately with a high standing-wave ratio in the transmission line. Holding the scale in this position with the knob, the dial, which is friction-coupled to the knob, is set to read zero. With this done, any shift in the standing-wave pattern along the transmission line can be found by resetting the knob until the edge and center of the scale again coincide with the same minima. The shift and its direction is indicated directly by the dial, which may be calibrated in degrees, radians, or decimal parts of a wave length.

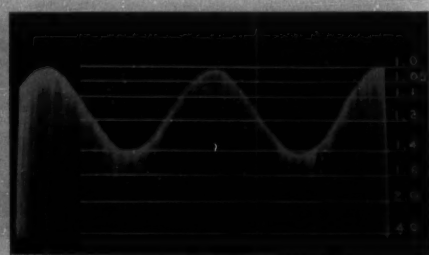
Features and Applications

With the travelling probe operating automatically, and a continuous picture of the standing-wave pattern appearing on the screen of the indicator, a number of unusual advantages of the instrument become apparent. One hand has been freed from the task of manipulating the travelling probe and thus is available for other operations or adjustments. With the cathode-ray tube indicator, the VSWR is found more conveniently, and as accurately, as with the meter used usually for this purpose. In addition, no difference computations and conversions are required to determine the amount of shift of the standing-wave pattern, since this can be read directly from a dial, the calibration of which is independent of the actual wave length. In addition to the rapidity with which measurements or adjustments on components can be made, the fact that the device is active actually adds interest to the measuring process.

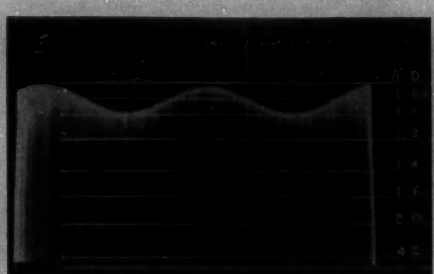
One of the simplest yet perhaps the most valuable applications of the automatic standing-wave indicator is found in its qualitative picturing of the effects on the standing-wave pattern caused by changes or adjustments made to line components or terminations. The results have to be seen to be fully appreciated, but a description of typical examples will have to suffice here. For instance, the adjustment of a termination for optimum standing-wave ratio with the automatic standing-wave indicator can be accomplished often in a matter of seconds. The effect of adjustments is immediately seen on the cathode-ray indicator, and adjustments are carried out in the direction of lessening standing-wave ratio until a matched condition is reached or most nearly approached. The standing-wave



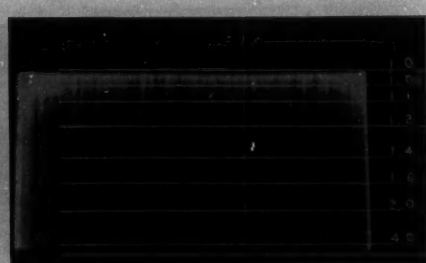
(A). VSWR of 2.0



(B). VSWR of 1.4



(C). VSWR of 1.1



(D). VSWR of 1.0

Figure 4. Typical standing-wave patterns

patterns progress from the stage shown in Figure 4(*A*), through (*B*) and (*C*), to pattern (*D*), indicating unity standing-wave ratio. Very frequently, this is all that a standing-wave indicator is required to do, as when adjusting tuning stubs, impedance transformers, plungers, line stretchers, crystal mounts, and so forth. In this operation, both hands are free to make the adjustments on the component. Similarly, production adjustment of components can be carried out rapidly in this manner, and individual checks of optimum standing-wave ratio are indicated quickly.

An interesting demonstration showing the effect of variations in detector terminating impedance can be performed with the setup shown in Figure 1 by observing the change in the standing-wave pattern as a simple resistive load across the crystal output is varied.

Impedance plotting is simplified when using the automatic standing-wave indicator, because of the ease with which the amount of shift in the standing-wave pattern can be found directly in the desired terms of wave length or angle.

If for some reason, the automatic standing-wave indicator cannot be used to advantage in obtaining the required information, such as when measuring very high standing-wave ratios, the probe may be operated manually in the ordinary way, and the amplifier meter used for indications.

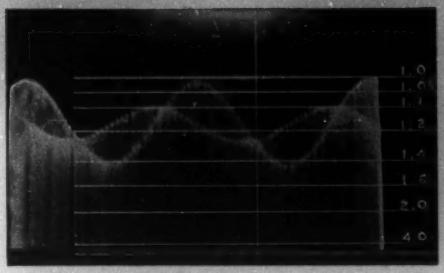


Figure 5. Two simultaneous standing-wave patterns demonstrating effect of recurrent change in terminating impedance

Another novel feature of the automatic standing-wave indicator is its ability to indicate simultaneously multiple standing-wave patterns. For example, Figure 5 illustrates two simultaneous standing-wave patterns caused by recurrent changes in the terminating impedance. The particular pattern shown was generated by applying a small modulating square-wave voltage to the crystal detector terminating the transmission line shown in Figure 1.

The effects of certain types of transmission-line switching devices can be similarly observed. Still another application of the multiple-pattern scheme permits simultaneous observations of the standing-wave patterns arising at different generator frequencies, as the different frequencies are rapidly switched in succession. Separate klystrons may be used for this purpose.

The foregoing examples of the capabilities of the automatic standing-wave indicator serve also to illustrate its utility as an instrument for lecture or classroom use in making dramatic demonstrations of standing-wave phenomena.

No Discussion

Recent Research on Pulsed Light Sources

W. T. WHELAN
ASSOCIATE AIEE

Synopsis: The determination of the dynamic characteristics of arc discharges lasting for less than two microseconds, and recent developments in the application of hydrogen thyratrons to stroboscopic light sources are reported. It is pointed out that electrical circuit conditions prevalent in single flash or low repetition rate gaseous discharge lamp systems are considerably different from those existing in high repetition rate, multiple-flash circuits. Improvements in multiple-flash systems are described and their synchronization with high-speed cameras which use internal optical shutters is explained. Certain electrical features of a recent high-speed motion picture system utilizing synchronized flashlighting are reviewed. The need of further research and development on gaseous flashlamps is indicated.

RECENT technological developments have created increased interest in the art of high-speed photography. Single pictures of self-luminous phenomena in which exposures as short as 1/100 of a microsecond are realized with the use of a Kerr cell shutter. Classic spark shadowgrams with exposures of the order of $^1/_{10}$ microsecond are common in ballistic research. Single exposure, stroboscopic photography which has been made possible by the introduction of modern high-pressure gaseous discharge lamps and exhibits effective exposures ranging from 100 through several thousand microseconds is becoming commonplace. The desire to take motion pictures of short-lived events has led several manufacturers to introduce high-speed motion picture cameras capable of several thousand pictures per second with exposures varying from 50 to several hundred microseconds. Edgerton and his associates have introduced systems which combined fast motion picture cameras and gaseous discharge, stroboscopic light sources. The present paper recounts investigations carried out at the Naval Ordnance Laboratory at White Oak, Md., on this general problem of combining these flashlamp light sources with commercial high-speed cameras. The greatest part of the work was directed toward developing circuits which would permit operation of the flashlamps in synchronism with commercial cameras at higher voltage and consequently higher power input levels than those attempted previously.

Since action stopping by means of short exposure is the real justification of the complexity of synchronous pulse lighting it is obvious that extreme brevity of the light output is required. For ballistic research, exposures in excess of 1 or 2 microseconds are considered too long. In this respect the pulsed light sources discussed here differ from the longer duration single flash counterparts. The manner in which photographic negatives integrate total light to effect their exposure requires extremely large peak light output in order to produce useful photographs when the total luminous interval is made short. Because the light output is a function of the electrical input, this demands a relatively large peak power input during the flash. The level is of the order of 1 megawatt.

All of these pulsed light sources are excited by capacitor-fed discharges or modifications thereof. One of the notable variations is the transmission line-fed discharges in which the capacitor bank is replaced by a charged transmission line. The importance of such a system is that the electrical pulse shape and length is controllable by the proper choice of transmission line parameters.[1] Other circuit details remain unaltered. At the Naval Ordnance Laboratory, this latter method has been used with some success. A future report is planned.

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W. T. WHELAN is with the Naval Ordnance Laboratory, Washington, D. C.

Electrical Characteristics of Flashlamp Loads

A study of the input power and load resistance variations during the discharge was the first undertaking in this series of investigations. The reason for this was that the available information on the pulse behavior of flashlamps was meager, contradictory, and specifically applied only to single-flash discharge circuits. The behavior of these single-flash circuits is considerably different from the practical multiple-flash circuit because of the much larger capacity of the energy storage capacitors used with the former circuits. For this reason, it was feared that what information was available was inapplicable. Larger capacity with the resultant higher discharge energy is permissible in nonrepetitive systems because lengthy rests occur between operations and the average energy input is kept small. As a result of the very large capacitor used, these single-flash circuits are always highly overdamped and the electrical discharge can be considered as simple R-3 transients, that is, wiring inductance is of little concern. In this way, apparent average values of the discharge resistance have been computed from the length of the discharge current pulse. For high-speed multiple flash service, however, two requirements demand the use of smaller storage capacitors: (1) because of the higher repetition rate, the input energy per flash must be reduced; otherwise, flashlamp destruction from excessive heating will occur; (2) a short duration flash of 2 microseconds or less is desired and a reduction of capacity will result in shorter discharge pulses. The reduction of storage capacitance can be better appreciated by considering the usual range of capacitance values used in single-flash service with the normal multiple-flash service; the former range commonly from 10 to 500 microfarads while the latter seldom exceed 0.1 microfarad and are sometimes as low as 0.01 microfarad.

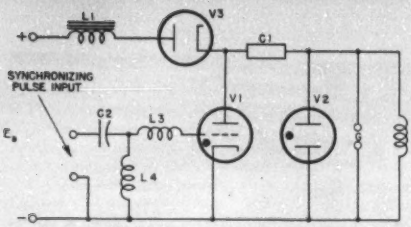

Figure 1. Typical pulse modulator circuit

V1 = Hydrogen thyratron, type *5C22*
V2 = Flashlamp, types *FT-121, FT-125* or *FT-127*
V3 = Clamping diode. Pair of parallelled type *8020's.*
G = 20 kv protective gap
C1 = Energy storage network
C2 = Pulse coupling capacitor
L1 = 2-henry charging choke
L2 = 1-millihenry closing choke
L3 = 100-microhenry noise suppression choke
E_B = 6–8 kv power supply

Actual determination of the lamp resistance during the short-lived discharged pulse was made by taking simultaneous oscillograms of the voltage across the arc and the current through it. The oscillographs used were of the fast-sweep, synchronoscope type and employed the new type high-intensity cathode-ray tubes. These tubes operate at 15 kv over-all accelerating potential and permit photographing of transients in which the spot velocity exceeds 50 inches per microsecond. A special 0.1-ohm current shunt was devised which had less than 0.001 microhenry effective series inductance. The suppression of the series inductance was necessary to reduce the shunt sensitivity to the rate of change of the surge current. No amplifiers were used because the current and voltage amplitudes were more than sufficient to deflect the oscillograph beam. Time variations of the apparent arc resistance were obtained by a point-by-point division of the arc voltage drop oscillogram by the arc current oscillogram. Investigation of the commercial flashlamps suitable for multiple-flash service showed that the dynamic load resistance of the conducting gas column, as determined from the oscillograms, fell extremely low during the impulsive discharge. Actual measurements indicated that values of this resistance at crest current ranged from 1 ohm to about 10 ohms, depending on tube type in use. The instantaneous power input was obtained by multiplying the two oscillograms. Measurement of the light output required the use of amplifiers; special video amplifiers with an 8-megacycle bandwidth were used. The light-to-electrical signal transducer used was the common 9-stage photomultiplier tube. Wideband response to match the amplifier was provided in its circuits.

The inevitable lumped wiring inductance encountered in practical setups, together with the small values of capacitance used in multiple-flash service resulted in discharge circuits which were nearly always underdamped. As a consequence, the wiring inductance is important in limiting crest current and the pulse width is determined by the wiring inductance and the storage capacity. Oscillatory discharge current would result if a bilateral switch were used in these circuits to connect the storage capacitor bank to the lamp load. This condition is an important factor in the selection of the switching device since an oscillatory discharge would make possible uncontrolled multiple flashes of the lamp and an accompanying lengthening of the flash duration.

Switch Problem

This single requirement that the switch must be a rectifier highlights the switching device problem upon which the ultimate success or failure of any high-speed multiple-flash system rests.

Peak currents ranging from 500 to 2,500 amperes were measured when operating at the high voltages used; thus a switch was required which exhibited extremely low series resistance and high current-carrying capacity. In addition, the switching device had to handle these surge currents at rates as high as 3,000 times per second under normal operating conditions. Normal crest voltages of 10 to 15 kv were switched and recovery rates as high as 50 volts per microsecond were observed. This meant that recovery time of the switch had to be extremely short.

As a starting point in the choice of a switch to meet the foregoing requirements, the usual switching methods which had been used successfully in other stroboscopic light systems were tried for adaptability. Rotary gap switching which proved popular in early radar proved too noisy, unpredictable, and incapable of ready synchronization. Triggered stationary gaps were useful also but suffered from time "jitter," burning, and excessive noise. The use of small, glass-enclosed ignitrons, while good at low repetition rates, failed from overload at high speeds. Inconvenience of their necessary vertical mounting and sensitivity to mechanical shock made them unsuitable for portable gear. The hydrogen thyratron impulse switch, which was developed to replace the rotary gaps in war-time radar, at first appeared as an unlikely possibility. This was because the peak current ratings of the two largest types were only 90 and 325 amperes. However, because of the simplicity of synchronization, the larger of the two types, the *5C22*, was tried. After considerable experimentation, it was found that this tube could be made to commutate 2,500 ampere surges at 3,000 cycles per second with no apparent damage. These results were certainly unexpected from a consideration of the tube ratings. Cathode sparking was noticed, but useful tube life was realized with little evidence of cathode disintegration. Spurred by this success, the small type *4C35*, which possessed a 90-ampere

Figure 2. The pulse modulator unit

Figure 3. Flashlamps which have been used successfully with the modulator

peak rating, was tried. Again successful switching was realized.

Camera Synchronization

At this point the question often raised is why should one ever use cameras with internal shutters when the flash duration, itself, can be used to provide shutter action? The answer is not immediately apparent and a digression must be made here for the answer to this objection. It should be realized that the high-speed type camera utilizes continuously moving film which normally is accelerating throughout the motion picture, and may reach a speed of 100 feet per second. As a result, the most important motion which must be "stopped" is that occurring between the small image formed by the lens system and the moving film itself rather than that of the moving objects in the field. A small fraction of a millimeter relative motion between image and film may represent an equivalent apparent motion of many inches of the photographed objects, including even the immobile objects. An exposure as short as one microsecond results in sufficient blurring from image motion to degrade picture quality. In those cameras using internal optical shutters, this unwanted effect is practically eliminated by the shutter system which consists usually of a rotary compensating prism which moves the optical image in such a manner that relative motion between film and image is eliminated. Such a system, together with microsecond exposures of the actual moving event being photographed, makes possible exceedingly high definition photography. For optimum compensation and proper frame width in such a system, it is essential that the light pulse occur at the instant when the internal shutter is at the "wide open" position. Continuously accelerating film also requires a corresponding shift of flash rate, if properly spaced pictures are to be realized.

Each of the commercial cameras which had internal shutters presented a different synchronization problem. The best solution consisted of small synchronizing alternators mounted on the shaft of the main camera driving motor. This procedure was adopted in order that the delicate mechanical features of the camera mechanism might be disturbed as little as possible. Operating on the variable reluctance principle, the auxiliary synchronizing alternator consisted of alternate ferrous and nonferrous slugs which passed between the poles of a small Alnico V magnet. The magnet was wound with a pickup coil in which a signal was induced as a result of the flux pulsations. It was a simple matter to choose the number of slugs so that the coil signal frequency corresponded to the shutter frequency. Proper phase relationship between synchronizing signal and the shutter was accomplished by actually changing the angular position of the magnet with reference to the shaft. This synchronizing signal was then available for use with whatever type switch might be selected.

Performance Tests

No serious attempt was made to discover the top switching rate because an upper limit of 4,000 frames per second exists, set by the available commercial 16 millimeter cameras, but success was experienced at 5,000 cycles per second although the operating voltage had to be reduced to prevent premature flashlamp destruction.

In attempting to explain the wholly unexpected success of the thyratrons when loaded so far beyond their ratings, it must be pointed out that the duration of the discharge in these circuits is always one microsecond or less and that the usual picture-taking interval is less than three seconds in length. Such short bursts of service, while adequate for motion pictures, do not add up to an impressive tube life figure when stated in hours, but in terms of pictures taken, a truly "long" life is had. A typical tube was used in over 200 motion pictures which were taken on 100-foot reels of 16-millimeter film. During this time, it operated at an average rate of 2,500 flashes per second for two seconds per reel. At the end of this time, the tube, apparently still good, was replaced as a matter of routine. Unfortunately, no rules can be set regarding tube life under such strenuous conditions of service, but it is felt that the example cited is representative of what can be expected. Thyratrons from several different manufacturers have been used with no interchangeability problems. In any case, the tubes have repeatedly outlived the quartz lamp units in service.

The usual pulse unit in use at the Naval Ordnance Laboratory uses the *5C22* thyratron instead of the smaller *4C35*. This choice was made because of an annoying grid "spike" voltage which appeared when operation above 10 kv was attempted. This spike apparently is the result of the initial breakdown occurring between plate and grid, rather than plate and cathode and seems to be intensified by cathode sparking. This grid spike caused heater-cathode punctures in the cathode follower driver which fed synchronizing signal to the thyratron grid.

The use of the hydrogen thyratron prevented underdamped discharge circuits from oscillating as hoped. This effect, desirable as it was, made it necessary to limit the self-inductance to as low a value as possible; otherwise, excessive underdamping caused the energy absorbed in the arc during the single half-oscillation permitted by the thyratron to fall to low values, and this of course resulted in a decided falling off of light output. Practically, this means that the effective lumped series inductance had to be limited to less than five microhenries when the available lamps and the usual range of storage capacitors were used. It should be noted that this adjustment of circuit damping is best carried out by control of the effective series inductance because neither the operating capacity nor lamp

Figure 4. Hydrogen thyratron switch tubes 5C22 (left) 4C35 (right)

resistance admit large modifications in these circuits.

In the circuit used, a slight underdamping was found actually desirable because it resulted in a reversal of the instantaneous thyratron plate-to-cathode voltage immediately after discharge, and this negative "overshoot" was helpful in aiding rapid deionization of the switch tube.

Circuit Discussion

A brief review of one of the several possible pulse circuits follows: As shown in Figure 1, this pulse modulator circuit resembles closely certain high-level radar modulators. Since the basic circuit can be found in any radar textbook,[3] only a brief résumé of its operation is made here. V1 is the hydrogen thyratron switch tube and V2 is the gaseous light source. L1, L2 and V3 constitute the charge-up circuit and C1 is the energy storage network. The direct power supply, E_B, is connected to the positive and negative terminals. The purpose of the diode, V3, is to permit operation of the light source at a cyclic rate lower than that set self-oscillation frequency of L1 and C1.[3] C2, R1 and L4 form a coupling network which delivers the synchronizing signal to the thyratron grid. L3 is a grid suppression choke coil which prevents hash from being fed back into the driver stage. G is merely a protective gap which prevents overvoltaging C1 in the event that modulator load, V2 is inadvertently disconnected.

A typical cycle of operation may be reviewed. Prior to arrival of the synchronizing pulse, C1 has been charged to approximately $2E_B$ and prevented from discharging to the power supply voltage, E_B, by the "clamping" diode, V3. At the proper instand a steep-fronted, positive, trigger pulse of 150 volts amplitude is applied to the grid of V1 which causes this thyratron to break down. Firing of V1 connects C1 across the flashlamp, V2, and since the voltage to which C1 is charged is more than sufficient to force the breakdown of the lamp, V2, C1 discharges through with the resulting light output. Very little energy passes through L2 which parallels the lamp because of its high impedance to the pulse voltage. L2 must be capable of withstanding the full pulse voltage across its terminal, however. On the next recharge cycle, C1 is brought to a final voltage of nearly $2E_B$ by the charging choke, L1, and the system again awaits the synchronizing pulse.

It will be noticed that an inductive charging circuit is used instead of the more usual charging resistor of low repetition rate systems. In multiple flash systems, this complication is necessary because the large amount of power required by each lamp results in too great a power loss in the low efficiency (50 per cent maximum possible) resistance charging system. A single lamp operated 2,000 times per second with a storage capacitor of 0.05 microfarad, charged to a peak voltage of 15 kv, represents an average power dissipation of 2.25. Using resistance charging, a minimum power requirement of 4.5 kw would be imposed, while an inductive charging system would demand only the load power plus small circuit losses. The inductive charging circuit and clamping diode operates also at various repetition rates with no adjustments required, while the resistive charging networks cannot be so operated. This type of variable frequency operation is desirable because the camera speed is adjusted readily to any value less than its rated maximum and is frequently so operated for special purposes. The only drawback to this method of charging is the difficulty of obtaining commercially a reactor which has sufficient current rating and the requisite insulation across the winding as well as from winding to ground, and the need of the high-current high-vacuum diode which possess an inverse voltage rating of 10 kv, or more.

System Features

The modulator outlined in Figure 1 has been used also for multiple-spark operations. Repetition rates of 2,000 cycles per second were obtained successfully with no marked decrease in thyratron life, even though, because of the extremely low resistance of the spark gap, a larger amount of energy was dissipated in the thyratron than when used with flashlamps. Light sources consisting of short gap sparks are useful in certain types of ballistic photography because they can be readily converted into intense point-sources of light. No known previous attempts have been made to operate a spark at high repetition rates for photography purposes in this manner. The multiple spark source awaits development of suitable photographic techniques before its unique features can be exploited as research tools.

In addition to the flash synchronizing circuits and high level pulse forming circuits just described, a practical picture taking system requires a considerable amount of auxiliary cycling and interlacing equipment. Most photography problems require the use of several lamps to provide lighting free of unwanted shadows. In the simplest problems the several lamps may operate simultaneously, but in the majority of cases, where large fields are to be covered, progressive sequence lighting which "follow" the action across the field is desirable. The control equipment developed at the Naval Ordnance Laboratory allows the operator to preset any sequence of lighting which experience dictates will be necessary. Control of the individual light sources is exerted conveniently by means of grid block switching of individual buffer amplifiers which deliver the synchronizing trigger signals to the thyratrons. Through self-contained dividers, the operator can select any lamp or group of lamps for operation at subsynchronous rates. Such lighting offers varying degrees of contrast on successive frames; this feature is sometimes highly desirable in motion pictures taken for frame-by-frame analysis of a phenomenon.

Power supply considerations are a relatively important item in any multiple lamp pulsed light source system. As has been mentioned, the average power which must be delivered by the direct power supply exceeds several kilowatts per lamp when the picture is being taken. However since the duty cycle of the power supply is so small, it is obviously not necessary to provide the full continuous duty capacity for these short term demands. It is important, nevertheless, that the regulation of the power source be small, otherwise unwanted interaction of individual lamps will occur. Transformer-rectifier sets were chosen instead of rotary machinery because of the high d-c voltage (7,000 volts) used. A full wave, 3-phase bridge rectifier circuit was chosen because of its many desirable features.[4] Single phase rectifier operation to multilamp installation is not advisable. Individual power supplies were built as units capable of delivering 15 kw each because of the greater handling convenience and availability of its component parts. For load demands in excess of this power, additional power units were used. A great advantage of this type of power supply is that failure of a single rectifier bank does not render the lighting system completely inoperative. No d-c filters were used on the power supplies because the low level of the output ripple from the 3-phase bridge connection was not objectionable.[5]

Radio-frequency interference propagated over the power lines proved troublesome. This was reduced by appropriate filters and the use of the polyphase power supply. Radiated inter-

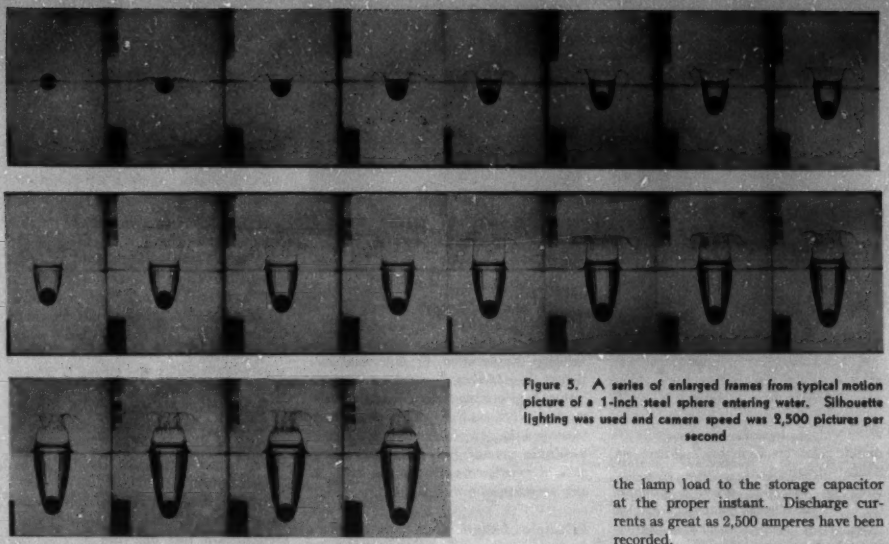

Figure 5. A series of enlarged frames from typical motion picture of a 1-inch steel sphere entering water. Silhouette lighting was used and camera speed was 2,500 pictures per second

ference from the actual discharge circuit was suppressed by the use of coaxial cable to connect lamp and modulator. Ordinary *RG8*/U-type cable was used with success if the length was kept less than 20 feet. Longer sections of line occasionally gave trouble because of reflections which arose from improper termination. Excessive attenuation did not appear to be a problem. The polyethylene insulation of these cables is sufficient to resist 25-kv surges caused by mismatch terminations.

As a result of the present series of investigations, it is clear that further development of the quartz discharge lamp is required. The presently available lamp types are excessively fragile and are subject to frequent breakage from improper handling. The input per flash at the higher repetition rates should be greater in order that the number of lamps required for a specific "take" might be reduced, but this is impossible with existing lamps. The gas physics of these lamps seems to be satisfactory, since the duration of the deionization tails was not found to be excessive. While the present thyratrons are usable as switch tubes, it is desirable to have larger ones available possessing cathode emissions that will permit operation within ratings.

The investigation was sponsored by the office of Naval Research.

Summary

A system employing pulsed gaseous light sources designed to operate in synchronism with high-speed motion picture cameras has been developed. A distinct improvement over previous systems with regard to definition and flexibility has been realized. This type of operation is made possible by precise synchronization of the internal camera shutter and the firing of the flashlamp. The operating voltage of the lamps is of the order of 10 to 15 kv, which is considerably higher than previous flashlamp systems used. As a result of the higher operating voltage, lower capacity energy storage capacitors are possible and light pulses, whose durations are as low as one microsecond, result.

As many as 3,000 to 4,000 pictures per second can be taken with presently available cameras and flashlamps; no lower frequency limit exists. Unexpected success has been realized with hydrogen thyratrons serving as switches to connect the lamp load to the storage capacitor at the proper instant. Discharge currents as great as 2,500 amperes have been recorded.

The system has been designed so that a number of commercial flashlamps can be used interchangeably without circuit alterations. In addition, multiple-flash spark gaps have been successfully substituted for the flashlamp. Operational convenience and flexibility have been incorporated into the system by way of compact, portable, high-voltage lamp units and elaborate interlacing and sequence circuits for light control. Power supplies are designed for operation from standard 208-volt 3-phase lines. A need for thyratrons of larger current capacity, and flashlamps which are mechanically and electrically more rugged is apparent from this investigation

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No Discussion

The Magnetic Fluid Clutch

JACOB RABINOW
NONMEMBER AIEE

AN apparently new type of magnetic fluid and several classes of new devices utilizing this fluid have been developed at the National Bureau of Standards. Among them are new types of hydraulic actuators, dashpots, and clutches. These developments have stemmed from an extensive research program on electronic digital computers for the office of the chief of ordnance, Department of the Army. Such machines require electromechanical input and output mechanisms which employ, among other elements, magnetic recording tapes and their associated transfer devices. In some of the applications, extremely rapid accelerations are necessary, with frequent starts, stops, and reverses. Since it is desired that velocities of the order of 20 feet per second be reached in a few milliseconds, a search was made throughout the field of fast-acting motors, clutches, and brakes in order to find equipment suitable for this duty. One of the items found was the clutch of W. M. Winslow, which uses electrostatic attraction between finely divided insulating particles suspended in oil and two conducting clutch plates.

The electrostatic clutch suggested the possibility of devising a new type of magnetic clutch, using two magnetic plates and substituting a suspension of magnetic particles for the electrostatic mixture. A new material and several new devices have resulted from these experiments. The most obvious application of the principle has been in electromagnetic clutches, but the electromagnetically controllable mixture appears to offer considerable promise for other uses as well. When used as a filler in a dashpot, the suspension of iron in a suitable liquid offers great advantages in that, by the employment of suitable electromagnetic control, the characteristics of the dashpot can be made to be a function of position, velocity, acceleration, or any combination of these factors.

Simple electromagnetic valves, with no moving parts, similarly can be employed to control the flow of this mixture through hydraulic systems, thus presenting a new area of experimentation in the field of servo-mechanisms.

Principle of the Clutch

The magnetic fluid clutch operates on the basic principle that when the space between two parallel magnetic surfaces is filled with finely divided magnetic particles and a magnetic field is established between the two plates, the magnetic particles bind the plates together against movement parallel to their surfaces. The magnetic particles may be finely divided iron which, for most applications, is mixed with a liquid such as oil, to prevent packing and to afford smoother operation of the clutch. When a portion of this mixture is acted on by a magnetic field, the iron particles are mutually attracted, bind together in the field, and the mixture seemingly "solidifies"—an effect readily demonstrated by lowering a small permanent magnet into a beaker of the iron-oil mixture (Figure 1). As the magnetic field can be produced by an electric current, a very simple means is thus obtained for the control of the binding force over a very wide range.

Advantages of the Magnetic Fluid Clutch

Preliminary results at the National Bureau of Standards indicate that the electromagnetic fluid clutch has numerous advantages over many other existing types. It is characterized by extreme smoothness of action because all contacting surfaces, both of the plates and of the iron powder, are coated by a lubricant. The clutch is easy to control and requires very small amounts of electrical power. The control is extremely smooth from the minimum, which is determined by the viscous drag of the oil, to the maximum, which is controlled by the magnetic saturation of the iron. Unlike other electromagnetic clutches, which follow a square law wherein the torque is proportional to the square of the current, the torque in the new clutch exhibits the square effect to a markedly less degree, and, in some clutches, the torque-versus-current curve is almost a straight line below saturation.

Another unusual and desirable feature found in these clutches, particularly when used at low flux densities, is that the value of static friction does not differ measurably from kinetic friction; hence, no discontinuities in torque exist at the instant of initiation of slip. This feature contributes greatly to the smoothness of the clutch action, since chattering in an ordinary dry friction clutch is mainly due to the difference between the static and kinetic friction.

Because it has no axially moving parts, the clutch is very easy to build, consisting essentially of a driving and a driven member which do not change relative position, except in rotation. As slipping occurs only between the extremely fine iron particles and between the iron particles and smooth face surfaces of the clutch, and as all the surfaces are lubricated, wear appears to be negligible. Moreover, if any of the surfaces are worn off, the iron dust thus generated simply adds to the iron powder already in the oil mixture. The gaps, as normally employed, are fairly large; therefore, any such wear will have minor effect. In the clutches tested at the Bureau, no wear has been noted, but because extensive life tests have not yet been run, it is not possible to rule out wear completely.

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JACOB RABINOW is with the National Bureau of Standards, Washington, D. C.

Design Factors

Before describing the several forms of magnetic clutches built and tested at the Bureau, it may be well to generalize somewhat on the design of such couplings. Figure 5 shows the general arrangement of an electromagnetic clutch where both the driven and the driving members are represented as a single magnetic unit. Figures 6, 7, and 8 show three alternate methods of dividing the magnetic structure into two parts for the transmission of power. It will be recognized that Figure 6 is the configuration most generally employed in dry magnetic couplings, while Figures 7 and 8 show alternate arrangements particularly suitable for the use of magnetic powder and oil instead of dry friction. Figures 9 and 10 are other arrangements where the magnetic structure is cut at more than two places so that parallel plate operations may be obtained in order to increase the area of action. While all figures show a single circular coil, it is quite obvious that separate coils, spaced like the poles of an electrodynamic machine, may be employed. The single coil is favored because of its simplicity of construction and excellent magnetic efficiency. Many other forms of magnetic circuits can be employed, depending on the materials used, results desired, cost, and other factors.

The design principles of the magnetic circuit of the clutch are no different from those used in all other electromagnetic machinery, with the possible exception that the value of the permeability of a suitable mixture of the carbonyl iron and oil is approximately eight times that of air. A permeability of eight implies that an iron-powder gap of, say, 0.080 inch has the same reluctance as an air gap of 0.010 inch. This increased permeability of the gap greatly simplifies the design and construction of practical electromagnetic couplings of the type described. The permeability was measured by means of toroidal coils wound around Lucite rings containing a known quantity of the magnetic mixture (Figure 2). Higher permeabilities can be obtained by using less oil, larger particles, or aggregates of large and small particles, but it is doubtful that very much better values than those reported can be obtained with carbonyl "E" powder mixtures.

Figure 2. Toroidal coils are wound around these Lucite rings

One of the main considerations in the choice of gap dimensions is the effect of viscous drag. For example, in a clutch of the type suitable for automotive use, the viscous drag of the clutch plates when the magnet is de-energized must be kept to a minimum, while in an overload clutch this effect may be of no importance.

The magnetic clutches thus far built at the National Bureau of Standards are of a basic and simple form, that is, with the spacing of the plates fixed, though this construction has the disadvantage of producing appreciable viscous drag at high speeds when the clutch is de-energized. For one of the bureau's experimental clutches (Figures 11 and 19), the drag is approximately 1.0 pound-inch at 1,000 rpm and varies linearly with speed. This drag may be minimized in several ways, such as using a thinner mixture of iron and oil, using lighter oil, increasing the spacing between the plates, and, of course, driving the clutch at lower speeds. Mechanical devices which change the spacing of the plates automatically when the clutch is demagnetized can be employed, and the number of other expedients is unlimited. Normally this viscous drag is a small fraction of the clutch torque, and the heat generated can be readily dissipated. In regard to automobile applications, the low idling speed of gasoline engines is advantageous.

In considering the design of magnetic circuits, it is concluded that the use of gaps in parallel, or single gaps of large area, entails large amount of iron and small copper losses, while the use of gaps in series reduces the amount of iron but requires more excitation to overcome the total resultant gap length. Which method of attack is preferable depends

Figure 4. The holding ability of the magnetized iron-oil mixture used in the new type of electromagnetic clutch is demonstrated in this experiment at the NBS. A 2-inch-square plate immersed in magnetic fluid energized by an electromagnet supports the weight of the girl (117 pounds)

Figure 1. A magnetic fluid consisting of fine iron powder and oil forms the heart of the National Bureau of Standards (NBS) magnetic fluid clutch

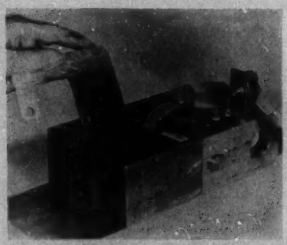
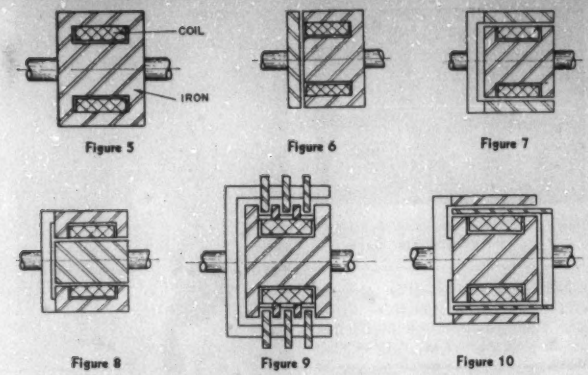


Figure 3. Laboratory test apparatus devised at the NBS to determine the shearing force that magnetic fluid used in the new type clutch will resist. The 2-inch-square iron plate (in hand) will sustain a pull of 200 pounds without moving when 10 watts of electric power are supplied to the coil



Figures 5–10. Cross-sectional drawings of several magnetic clutches

upon such factors as the availability of electric power, considerations of weight, and size. Multiple-plate clutches can be designed so that the sets of plates are either in series or in parallel. The 3-plate clutch of the design shown in Figure 21 (wooden mock-up) can be expanded readily to have any desired number of alternate driver and driven plates, as shown in Figure 25. This clutch, which uses the series gap arrangements, is very compact, but heat dissipation presents special problems, particularly if the clutch is to be used continuously in constantly slipping condition. Air or liquid cooling can be provided in any known manner.

Where low inertia is required in the driven member, as in high-speed servo work, it is desirable to make the exciting coil and magnetic iron a part of the driving member in so far as possible. Only a very thin member is then needed to pick up the torque in clutches such as shown in Figures 10 and 26. The thin driven plate, which normally is made of iron or other magnetic material so as not to introduce any unnecessary reluctance into the gap, may be made of various nonmagnetic materials as well. It was found that plates made of such materials, when introduced into the magnetic gaps, experienced torque because of the attraction of iron particles from opposite sides of the plates. Work on this effect is still in process, but it has already been found that mesh, or plates with perforations experience torques comparable to those obtained with iron plates. This effect is particularly important because, when thin iron plate is used, there is always the possibility that mechanical eccentricities may be greatly exaggerated by the magnetic pull and the plate deformed sufficiently to contact one or the other of the magnetic surfaces. This type of unbalance of the magnetic pulls is well known to the designers of rotating electrical machinery, and similar difficulties with bearing play and shaft flexibility may be experienced in the magnetic fluid clutches. The thin lip shown at the edge of the driven member of the clutch in Figure 26 was built to stiffen the cylinder. By using nonmagnetic materials this difficulty is obviated.

Another problem which arises in connection with these clutches is that of sealing the oil-iron mixture in its compartment. There are two separate places where such sealing generally is required. One is the point where the shaft supporting the internal member leaves the assembly, and the second, if an internal coil is used, is the point at which the leads are brought out. Good results have been obtained by using rubber "O" rings at the shaft bearing, but there is no doubt that other types of packing can be employed successfully. If there is danger of the iron particles getting into ball bearings, it may be desirable to add a magnetic lock beyond the oil seal to trap any particles of iron which get through the seal. The oil that passes beyond this point would then be clean and should have no harmful effect on the bearings. Such a magnetic lock may consist of a permanent magnet ring flanked by two iron washers, as shown in Figure 17.

Two other methods being tested for sealing the mixture into the clutch consist of using labyrinth seals, particularly when the clutch is to be used only in a horizontal position, and the use of spiral grooves on the shaft or on the bearing, arranged so that any mixture reaching the seal is automatically pumped back into the internal cavity of the clutch.

Generally speaking, it appears advisable to place the electrical winding inside the clutch for several reasons: this placement reduces the amount of wire needed; it puts the torque surfaces at the maximum radius; it permits better dissipation of heat generated by the friction surfaces; and it reduces the over-all size and inertia of the clutch. However, there may be times when these considerations are not important, as when seals present difficulties, particularly in the case of very thin shafts. The coils may then be placed on the outside of the friction surfaces. Such a design is shown in Figure 20.

It is possible also to make the energizing coil completely separate from the clutch so that the coil is not revolved but transmits the magnetomotive force to

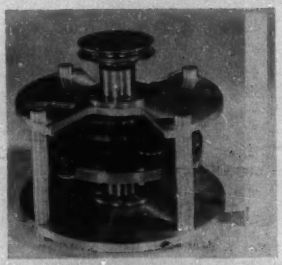


Figure 11. First NBS test model of practical clutch. Current supplied through the two slip rings energizes a coil in the disk-shaped central chamber, and hence through the magnetic fluid in the intervening space

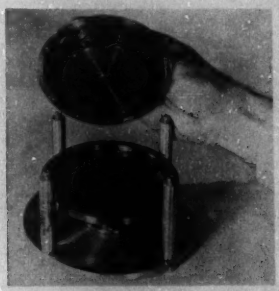


Figure 12. Disassembled view of the central chamber of the first NBS test clutch, showing the magnet coil on the upper plate surrounding the free center disk which connects to the driving shaft

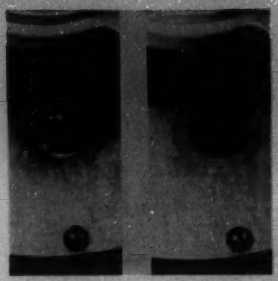


Figure 13. A close-up of a filling hole in the upper driven plate of the first practical model of the NBS magnetic fluid clutch. On the left the driving plate turns freely in the demagnetized mixture of powdered iron and oil between the upper and lower driven plates. When the clutch is energized, right, the magnetic flux acts on the iron powder suspended in the oil, and the mixture forms a virtually solid mass

the working faces through short air gaps. This can be accomplished at some loss of electrical efficiency and appears suitable only for very small sizes, where the power demands are low and the simplification is justified.

When an electromagnetic fluid clutch is used as a brake, slip rings are not necessary. Another very promising approach to the problem of their elimination is to employ a built-in a-c generator which may consist of no more than a coil mounted on one of the clutch members and arranged to pass through the field of a

Figure 15. The practicability of the magnetic fluid clutch was first demonstrated by immersing a small motor frame and field windings in a bath of powdered iron and oil. Application of current to the field windings causes the cylindrical iron rotor (substituted for the usual armature) to lock firmly

permanent magnet, or an electromagnet, mounted on the stationary frame of the machine. The alternating current thus produced may be applied directly to the clutch windings, or may be rectified before being applied. If nonlinear resistive elements are added to the circuit, interesting possibilities in controlling the clutch torque by means of speed variations are presented. If the field structure is mounted on the other rotating member of the clutch, the voltage developed will be a function of the difference in the speed of the two clutch members. While complete lock-in could not then be obtained, characteristics very similar to fluid couplings would be achieved.

Experimental Results

The first experiments performed at the Bureau consisted of taking the field structure of an ordinary small 2-pole motor, replacing the armature with a cylindrical iron rotor, and dropping the whole assembly into a beaker containing the iron-oil mixture. Voltage was then applied to the field winding and the locking torque on the rotor was examined. The torque was so great in this test that it was decided that the bearings must have "frozen" or that the rotor shaft had bent out of shape, although subsequent tests showed this not to be the case. A design as shown in Figures 11, 12, and 19 was constructed to check for the presence or absence of magnetic shear effect. This model was used for much of the experimental work at the Bureau. The bearings are external to the oil mixture, and, by using a vertical shaft, oil seals and their attendant friction are eliminated. The results indicated that the first experiments with the motor frame were quite valid in that large torques were no doubt due to the small gap present

This motor as later remodeled (Figures 15, 16, and 18) was used with several sizes of rotors to determine the effect of gap length on the resulting torque. This particular type of assembly, using the conventional motor frame with laminated magnetic circuits, is especially suitable

Figure 14. Test setup for observing the operation of the first practical model of the NBS magnetic fluid clutch

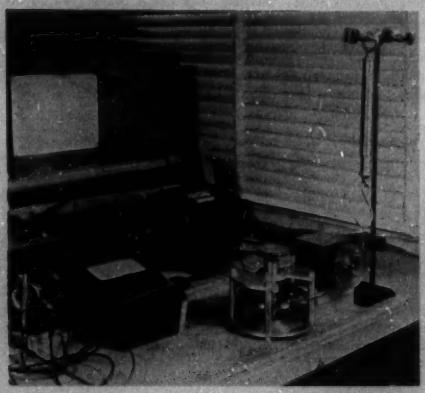
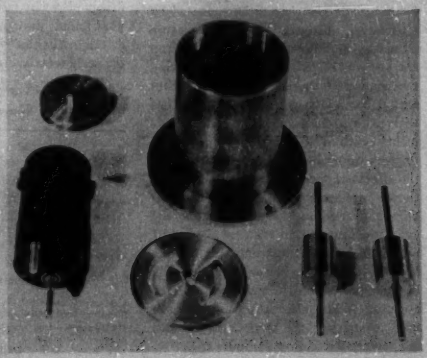


Figure 16. The apparatus used for the experiment shown in Figure 15 consists of motor assembly, two sizes of cylindrical iron rotors, housing, and cover



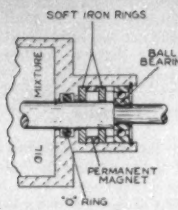


Figure 17. A design illustrating the use of a magnetic lock

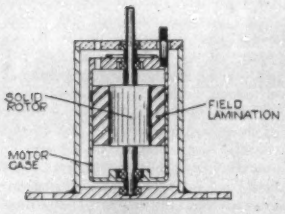


Figure 18. Cross-section of the small motor frame and field windings

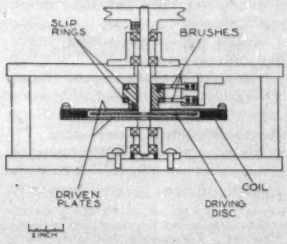


Figure 19. Cross-section of the test model of magnetic-fluid clutch

for magnetic clutches to be operated on alternating current. Using a mixture consisting of nine parts of carbonyl E iron and one part of oil by weight, the following torques were obtained: 2.5 pound-inches with a 0.062-inch gap; 3.0 pound-inches with a 0.025-inch gap; and 3.5 pound-inches with a 0.012-inch gap. These torques were obtained at low speed, and the effects of speed and of gap length are shown in Figure 27. In order to minimize the residual magnetic effects, the cylindrical rotors were made of 47 per cent nickel steel. This construction is suitable for d-c operation, while a laminated disk construction would be more suitable for a-c use. When alternating currents are used to energize the model, the torques are somewhat lower (Figure 28).

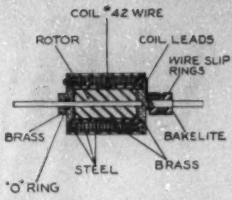


Figure 20. Design of a clutch with the electrical winding on the outside of the friction surfaces

Various experiments were performed with the flat-plate clutch (Figures 11, 12, and 19), the magnetic components of which are made of 47 per cent nickel steel. The inner driving plate is a disk 0.062 inch thick by 3 inches in diameter and the iron-oil gap on each side of the disk is 0.062 inch. The coil consists of approximately 200 turns of number 28 wire with a d-c resistance of 15 ohms. In the design of this clutch, no effort was made to make an efficient magnetic circuit, since the gap area is large relative to the cross section of the magnetic material surrounding each side of the coil.

Tests on this clutch indicate that the static friction is not appreciably different from the friction obtained under conditions of slip. This can be seen from a typical set of curves shown in Figure 29. The relationship between exciting current and torque is shown in Figure 30. The magnetization curves, it will be noted, are practically linear through zero. The clutch does not appear to obey the square law as is generally expected in magnetic devices. The reasons for this are not clearly understood. In one experiment with the flat-plate clutch (Figure 14), the torque was measured on a spring scale attached to a point on the periphery of the driven plates by means of a thin cable. It was found that when the clutch was driven by the electric motor the pointer on the scale remained completely steady, in-

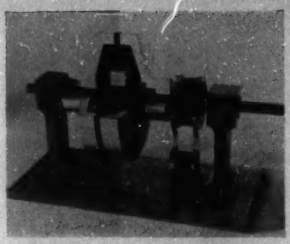


Figure 21. Cutaway model of a simple multiple-disk-type clutch. The electromagnet for energizing the magnetic fluid which normally fills the clutch casing utilizes the coil windings in the center of the clutch

dicating that the value of the torque did not drop with the increase in speed, an effect quite common in dry friction clutches and one which results usually in considerable instability under similar conditions of test. As may be expected, the clutch heated up appreciably during each run because of the energy dissipated in friction; this resulted in a lowering of the viscosity of the oil and a slight change in torque.

Determination of the type of liquid to use as a vehicle is based on such factors as difficulty in sealing, ability to withstand high and low temperatures, chemical stability, availability, and price.

Typical results of tests on different types of iron and various proportions of iron and oil in the mixture are shown in Table I. The behavior of the iron-oil mixture when magnetized and demagnetized may be visually examined through one of the filling holes of the flat-plate clutch. The liquid when unmagnetized looks like a heavy gray oil, as shown in Figure 13 (left), but when magnetized and subjected to shearing by revolving the inner plate, the material assumes a striated appearance as seen in Figure 13 (right). When the plate is moved

Table I. Torque of 3-Inch-Disk Clutch for Various Magnetic Powder Mixtures

Powder	Liquid	Proportions by Weight Powder	Proportions by Weight Oil	Torque in Pound-Inches With One Ampere, Direct Current
Carbonyl iron "E"	Light machine oil	5	1	7.7
	Light machine oil	4	1	5.7
	Light machine oil	3	1	5.5
	Light machine oil	2	1	4.5
	Light machine oil	1	1	2.3
	Silicone DC 500	10	1	8.0
	Silicone DC 500	2	1	4.7
Carbonyl iron "SF"	Light machine oil	4	1	4.3
Stainless steel flakes	Light machine oil	3	1	3.3
Hydrogen reduced iron	Light machine oil	1.5	1	2.3
Magnetic iron oxide	Light machine oil	1.7	1	1.1

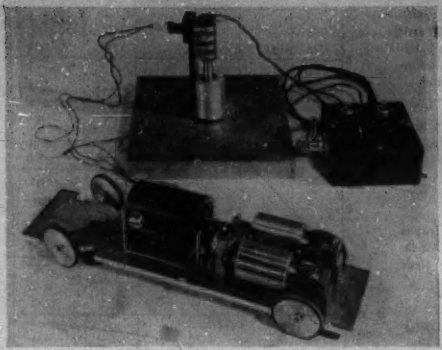


Figure 22. Close-up of the midget auto bus, with body removed, built to prove the principle of the NBS magnetic fluid clutch. At the top is the pivoted connector between the model and the control box which operates the model's miniature electric motor and clutches

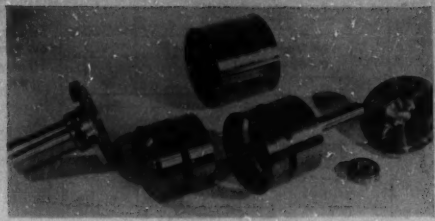


Figure 24. Exploded view of large electromagnetic fluid clutch (Figure 23). The parts are: (top) outer shell; (left to right) driving shaft, attached to end plate, with leads connected to the slip rings shown; inner member, attached to driven shaft; bearing and end-plate through which driven shaft extends

it appears that most of the shearing action occurs at the surface of the plate, with a great deal of rearrangement of particles. It has been suggested that different types of surfaces on the clutch plates may have interesting effects on the behavior of such clutches. Tests of these effects have not yet been made.

It has been found that in this particular clutch, a mixture of oil and carbonyl "E" iron, which is composed of particles with an average size of eight microns, gave the best results. This may not be true of other clutches or other conditions of tests. When dry powder is used in the clutch, the torques are of the same order of magnitude as when mixed with oil but eccentricity or misalignment of plates results in erratic operation.

Since the speed of response is of paramount importance in servo-mechanisms, the time of build-up and decay of torque was measured. This was done by means of a wire strain gauge attached to the rim of the driven member. The input voltage was applied to one pen of a multichannel recorder while the strain gauge was made a part of a 60-cycle bridge, the output of which was applied to a second pen of the recorder. The resulting record is shown in Figure 32. It will be noted that the torque rose and decayed in approximately 1/30 of a second. In some previous tests with similar equipment, torque-rise and torque-fall times of the order of 1/60 of a second were obtained.

Following these experiments, two small clutches (Figure 20) suitable for servo work were built into a model auto bus (Figure 22). In order to keep the construction simple, the coil was placed on the outside of the clutch proper. This construction required only one seal, achieved by using a small rubber "O" ring. The slip rings consist of copper wire wound in shallow grooves in a Bakelite drum. The wires were soldered together and turned in a lathe to approximately half their original thickness. This is also the type of construction used in the slip rings of the clutch shown in Figure 26. The coils, wound of number 42 enamel wire, have a resistance of approximately 12,000 ohms each. The magnetic circuit is made of 47 per cent nickel steel as in the two previous cases, with gaps of 0.032 inch. It was found that a current of eight milliamperes results in a torque of two pound-inches. This is the value at the knee of the saturation curve, with a value of three pound-inches obtained with

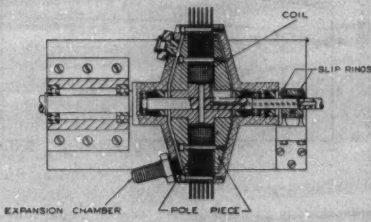


Figure 25. Cross-sectional drawing showing how a magnetic fluid clutch can be expanded to have any desired number of alternate driver and driven plates. This multiple-disk clutch is very compact

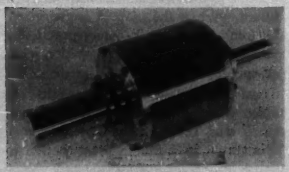


Figure 23. Large electromagnetic fluid clutch of the type suited to use in automobiles and machinery. This clutch, only 6 inches in diameter and $6^1/_2$ inches long, will transmit 40 horsepower at 3,000 rpm

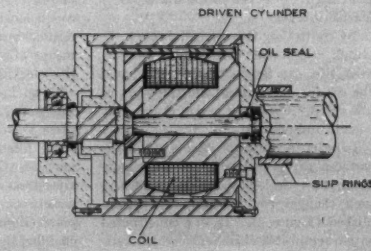


Figure 26. Where low inertia is required in the driven member, as in high-speed servo work, it is desirable to make the exciting coil and magnetic iron a part of the driving member in so far as possible. This drawing of such a clutch shows a very thin driven member, all that is needed to pick up the torque

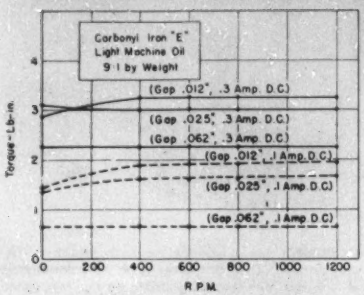


Figure 27 (left) Torque for various gaps—1- by-1¼-inch-cylinder clutch

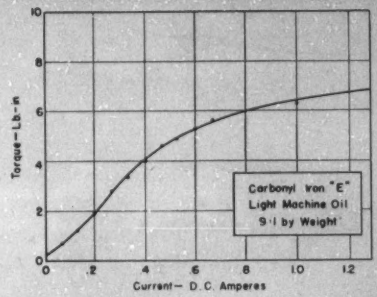


Figure 30. Torque of 3-inch-clutch — static test

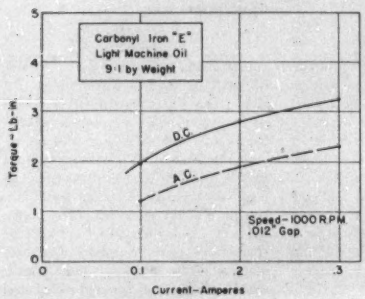


Figure 28 (left). Torques for alternating and direct current — 1- by-1¼-inch-cylinder clutch

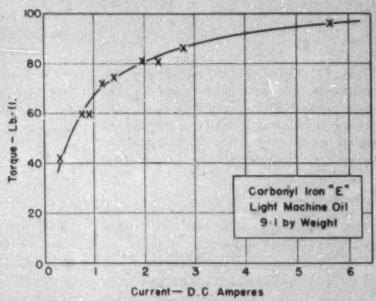


Figure 31. Torque of 6-inch-cylinder clutch

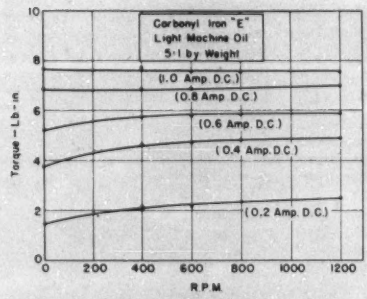


Figure 29 (left). Torque versus speed — 3-inch-disk clutch

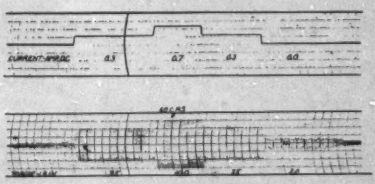


Figure 32. Time response for disk clutch

approximately 24 milliamperes. One of the clutches is locked to the frame and is used as a brake.

By controlling the current through the clutch and brake, acceleration and deceleration can be controlled easily, and the car can be made to run smoothly at low speed. The brake action is also very smooth and positive.

In order to study the effects of various surfaces, gap parameters, and other variables, it was decided to build a unit where such changes could readily be accomplished (Figure 3). It was constructed with a removable chamber in which various plates can be tested easily. The magnetic circuit is made of unannealed cold-rolled steel, resulting in large values of residual magnetism. This is overcome by means of a reversing switch provided in the electrical circuit. Using chambers with an internal width of 3/16 inch, it was found that when a 1/16-inch steel plate was inserted and the rest of the space filled with carbonyl "E" iron-oil mixture, the magnetic circuit reached the knee of its saturation curve with approximately 60 volts applied. The central plate (Figure 3) is kept centered in the chamber by four brass pins. It was found that the shear force on smooth plates, 1/16 inch thick, gave a maximum value of 20 pounds per square inch of surface. The rectangular plate gave values greater than those obtained later on circular plates due to the edge compression effects which it caused. When the magnet is energized, the iron-oil mixture becomes practically solid and can be held in any position without spilling.

Basing the design on the results obtained with this fixture, a much larger clutch was built in the bureau shops. This clutch is shown in cross section in Figure 26, and assembled and disassembled in Figures 23 and 24. The magnetic circuit again is of unannealed cold-rolled steel with the end plates made of dural. The coil has 600 turns of number 18 wire. Its total resistance is 3.5 ohms.

Figure 33. Low-inertia magnetic fluid clutch

In order to keep inertia of the driven member low, most of the magnetic circuit iron and the coil were made part of the driving structure. The driven member consists of a 1/16-inch-thick cylinder mounted on a brass disk which in turn is fastened to the driven shaft. Rubber "O" rings are used as seals as indicated in the cross section drawing. In the only test made so far, the clutch was mounted in a vise and the output shaft was turned by the use of a 3-foot lever. It was found that in this particular design, the static friction at high torques was not equal to the torque when the clutch was slipping, the first time this was found to be the case for such clutches. The reasons for the difference have not yet been ascertained. The change of torque amounted to approximately 25 per cent. However, the slipping torque, as observed on a spring scale driving the torque arm, appears to be very smooth. The relationship between torque and the magnetizing current is shown in Figure 31. Again no square-law effect is apparent. Additional tests on this clutch are under way.

In order to achieve higher torque-to-inertia ratios than in any of the clutches described previously, the clutch, shown in Figures 33, 34, and 35 was designed. Two symmetrical magnetic structures, slotted to minimize eddy current effects, are employed on both sides of a thin magnetic disk. The current-torque curve for this clutch is shown in Figure 36. Tests were made on the speed of response of this clutch when connected in the plate circuit of a vacuum tube. It was found that the clutch came up to a full speed of 1,270 rpm about 10 milliseconds after the grid of the vacuum tube was suitably energized.

Applications of the Clutch

The results obtained by the National Bureau of Standards in the experiments on the several forms of the magnetic fluid clutch suggest many possible uses for the new electromagnetic device. The automotive application mentioned previously is, of course, the most obvious one. Unlike fluid couplings of the type now common in automobiles, the clutch is not a speed-sensitive device. If the load is below the slipping torque of the clutch, no slippage occurs and the mechanical efficiency of the clutch is 100 per cent. The feature which particularly adapts the new clutch to automobile use is its easy controllability, which makes it especially attractive for use in automatic transmissions where permanently engaged gear trains are clutched in and out, depending upon the speed ratio required. Since the amounts of electrical power required to control the magnetic clutch are small, it is a simple matter to interlock the electrical circuits with the speed, throttle setting and power demands.

The field where these new clutches are expected to find their main application, however, is in servomechanisms. Here, friction clutches have been used, but their lack of smoothness, the changes in characteristics caused by wear, their nonlinearity, and the poor reproducibility of results have given rise to great difficulties. The new clutch should go far toward solving such problems. While servomechanisms can be operated by variable speed motors, hydraulic transmissions, and many other means, clutches and brakes have the very great advantage of low inertia-to-torque ratios. Electromagnetic clutches are particularly adaptable to serve as the final elements of electromechanical amplifiers.

It was found by experiment that the nature of the oil used in the magnetic fluid clutch has relatively little bearing on performance; hence, silicone liquids may be employed with excellent results, enabling the clutch to operate at very low and very high temperatures, an important consideration in military applications.

Another broad field of applications is in constant-torque and overload devices where the clutch is never de-energized. Permanent magnet clutches are particularly useful for such service.

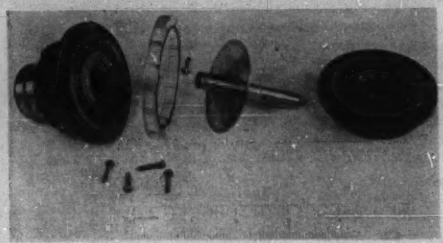


Figure 34. Low-inertia clutch—disassembled

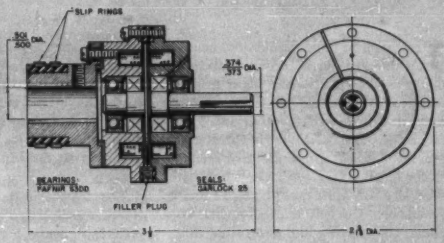


Figure 35. Detail drawing of low-inertia clutch

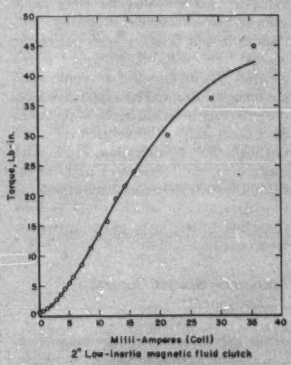


Figure 36. Current-torque characteristic of low-inertia clutch

No Discussion

Starter-Generator Control System for a Multijet Airplane

VICTOR B. HART
ASSOCIATE AIEE

THE starter and generator components used on a recently developed 6-engine jet airplane are in many respects identical to those that would be used on a reciprocating-engine-type airplane. One main difference, however, is the starter-generator itself. On a standard reciprocating-engine-type airplane, there are generally two units used, a starter and a generator. On this multijet-engine airplane, the starting and generating functions are performed by a single unit which is used both as a starter and as a generator. It is called a starter-generator, is rated at 400 amperes, 30 volts direct current, and is capable of supplying a continuous output of 400 amperes at engine speeds from 4,000–8,000 rpm. The electric system designed to control the starter-generator and its supporting components was patterned in the early design after comparable systems used on standard airplanes. Modification, and in some cases, complete redesign of parts of the control system were necessary in order to provide a system which would perform according to the more exacting requirements demanded of it.

Automatic Starter Control Disconnect

The first item that required consideration was the starting sequence. On standard airplanes, the starter generally becomes disengaged immediately after the engine has fired; however, this is not so when starting jet engines. Because of the starting characteristics of a jet engine, it is essential that the starter remain engaged not only until the engine has fired (which is at approximately 800 rpm) but also until the engine has reached a speed of revolution which will permit satisfactory engine acceleration without causing the engine to overheat. On the multijet airplane, the starter circuit becomes disengaged at approximately 2,700 engine rpm, and does so automatically to allow the pilot to give his undivided attention to the instruments and throttles.

The automatic starter disconnect feature is accomplished by utilizing a current relay as shown in Figure 1. This relay is a single-pole double-throw double-break relay that has had its coil replaced by a straight bus which carries the full starter current. It is set to pull in at approximately 500 amperes starter load and drops out when the load is reduced to approximately 200 amperes. The normally-open contacts are used to provide a hold-in circuit for the starter relays and, upon opening, de-energizes the starter. From the curves in Figure 2, it can be seen that as the engine speed increases, the starter-motor contribution to engine rotation decreases, with a resultant decrease in starter current. By simply adjusting the tension on the current relay's contact spring, the relay can be made to drop out at any predetermined engine rpm. The starter motor, therefore, becomes de-energized when its usefulness to the starting cycle is ended. Utilization of this drop-out feature is a primary advantage inasmuch as it permits "overlap starting." See Figure 3.

By the term overlap starting is meant the practice of beginning a starting cycle on one engine before the finish of the starting cycle of a previously started engine. Due to the fact that it takes approximately 30 seconds for the starter to accelerate the engine to firing speeds, it is possible for the pilot to utilize this time to work on another engine. This is precisely what the pilot has been able to do, thereby reducing the over-all time for starting all six engines. It was originally estimated that the very minimum time for starting six engines would be 15 minutes. However, by utilizing the automatic starter disconnect feature, the pilot has been able to start taxiing within five minutes from the time the first starter was energized. This represents a saving of several hundred pounds of fuel which is a very important item on this type of airplane and could very easily represent the difference between an incompleted and a completed mission.

Common Power Leads

The second main item that required consideration was that of utilizing common power leads for conducting power to and from the starter-generator. Originally the system used separate power leads for starting and generating purposes. Such a system is generally considered very satisfactory on most standard airplanes and was used on the initial control circuit on this airplane. However, after the complete control system had been "jelled," a weight survey of the system was made and it was found that the wire weight involved was way out of proportion to the value, or service the system rendered. Consequently, the circuit was redesigned to permit maximum utilization of the starter-generator power leads. To accomplish this, a switching arrangement was used to allow the use of the generator power leads while starting. This required the use of three 400-ampere relays and one *AN3025-2* differential reverse current relay per engine. Two of the 400 ampere relays (one starter relay and one generator relay) are located adjacent to the starter-generator while the other

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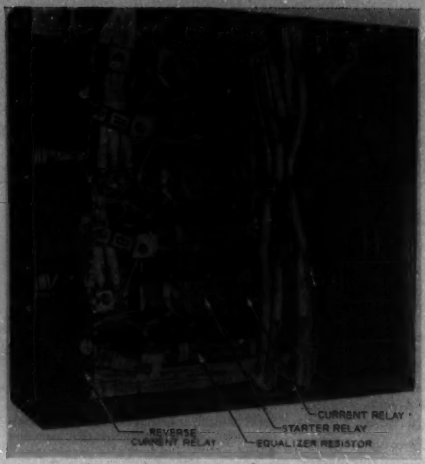
VICTOR B. HART is with the Boeing Airplane Company, Seattle, Wash.

The author wishes to thank the Boeing Airplane Company, Seattle, Wash., for permission to present these data.



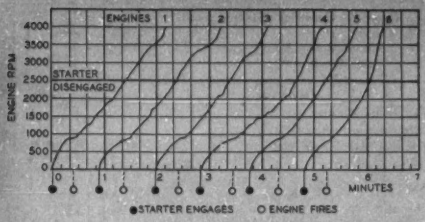


Figure 1 (left). Main power distribution shield (typical for each wing)

Figure 3 (above). Typical starting sequence recorded during a normal engine starting run

starter relay and the reverse current relay are located at the electrical distribution center in the body of the airplane.

Operation of the momentary starter switch energizes the two starter relays, thus switching the power leads to the starter circuit. After these relays have become de-energized, the generator relay picks up to switch the power leads back to the generator circuit. Subsequently, as the terminal voltage of the generator rises to exceed that of the bus, the differential reverse current relay becomes energized to connect the generator to the main bus.

The success of such a switching arrangement is predicated on the ability to prevent both the starter and generator relays from becoming energized at the same time. Many different types of switching arrangements could be used to achieve this requirement, and, of several methods tried, none was as simple and direct as that shown in Figure 4. It consists of controlling the generator relay through the normally closed contacts of both the starter switch and the current relay while, at the same time, it prevents shunting of the current relay during the starting cycle by utilizing the reverse current characteristic of the *AN3025-2* relay.

The reliability of a control system that utilizes switching relays in the main power leads to the generator at first appears to be questionable. However, when balancing the possible additional risks that may be involved against the amount of weight saved, there can be no doubt as to the value of such an arrangement. In the final configuration, it was possible to save from 30 to 40 pounds of weight per engine by utilizing this control feature.

When multiplying this value by six (for a 6-engine airplane) it can be seen that the total weight saved per airplane represents a large percentage of the total weight of the entire electric system.

Automatic Equalizer Disconnect

Another item in the starter-generator control system is the incorporation of an automatic equalizer disconnect. On most multiengine-type airplanes, the generator equalizer is controlled normally through the generator switch, the equalizer circuit being connected whenever the generator switch is in its "on" or normal position. Should a generator in such a parallel system become disconnected either by its overvoltage relay or its reverse current relay or just due to the engine being shut down, and should it also fail to have its generator equalizer system disconnected, its equalizing circuit would effectively send a signal to the regulating systems of the remaining generators to call for a reduction in their output voltages. The magnitude of this voltage reduction is approximately directly proportional to the number of generators removed from the line and amounts to approximately three to four volts when four out of six generators are lost as power sources.

Inasmuch as it is a standard procedure

Figure 2. Typical engine rpm and current curves versus time during starting period of jet engine

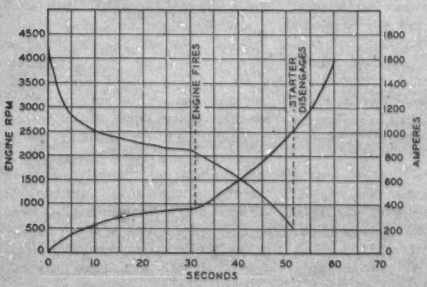


Figure 4. Starter-generator control circuit

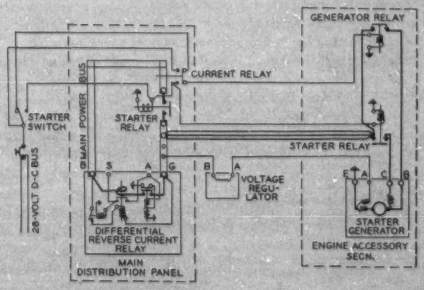
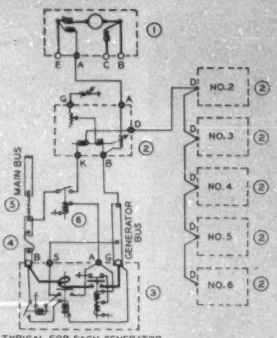


Figure 5. Generator equalizer circuit

1. Starter generator
2. Voltage regulator
3. Differential reverse current relay AN3025-2
4. Current limiter
5. Equalizer resistor
6. Equalizer cutout relay

for this type of airplane to reduce engine speed below 4,000 rpm (the lower limit for generator output) on all but two engines during a normal landing, it is most important that the available system voltage be maintained at its highest level. To insure that the main bus voltage will not be jeopardized by failure to disconnect a dead generator's equalizer circuit, the control for each circuit was transferred from the generator switch to an equalizer cutout relay (see Figure 5) which is controlled by the differential reverse current relay. Any time, therefore, that the reverse current relay becomes deenergized to remove its generator from the main bus, it will automatically deenergize the equalizer cutout relay to open its equalizer circuit.



TYPICAL FOR EACH GENERATOR EQUALIZER SYSTEM

Using a relay for control of the equalizer circuit has another advantage in that it can be located relatively close to the voltage regulator. This serves to shorten the lengths of the equalizer leads which not only reduces weight but allows for less lead resistance which means much better load distribution. This is borne out by the fact that the copilot has observed that the generator loads are distributed much more evenly than those encountered on other multiengine airplanes in which he has flown.

The remaining features of the control system, namely, overvoltage protection and indication, generator cutout due to either throttle cutoff or normal control, starter cutoff, and ammeter and voltmeter indication, have been purposely deleted. It was not intended that these features be discussed inasmuch as they are treated in much the same manner as those on reciprocating-type multiengine airplanes.

Conclusions

1. Incorporation of an automatic starter control system has made it possible to reduce the total engine starting time to a very minimum.

2. The utilization of common power leads for both starting and generating purposes has saved 215 pounds of wire per airplane.

3. Providing an automatic disconnect of the generator's equalizing circuit has allowed for maximum possible system stability during the loss of one or more generators.

No Discussion

Ground Power for Aircraft

JOHN H. BLANKENBUEHLER
FELLOW AIEE

Synopsis: The increasing use of electric apparatus on aircraft has brought about the need for rather large sources of electric power for these planes while they are on the ground without engines running. The development of these ground power units from the original storage batteries through the makeshift postwar era to the present highly specialized units is traced in this paper. Units from 5 to 60 kw now are used for this purpose. These units consist of portable engine driven sets as well as induction motor driven units for use in the hangars. These units must all be equipped with special voltage controls and with complete radio interference suppression. The larger units are furnished with a great variety of portability, some being self-propelled. These units must be built in 12, 28, and 120 volt direct current as well as 120-volt 400-cycle alternating current.

NEARLY all of the prewar airplanes were equipped with 12-volt d-c electric systems. They had a few instruments requiring electric energy, a few lights and an engine starter. The war brought about a great increase in the use of electric equipment on aircraft. Those planes developed during and after the war nearly all were equipped with 28-volt d-c systems. During and immediately after the war these planes were serviced on the ground by the use of battery carts containing either 12- or 24-volt storage batteries which in some cases could be switched from one voltage to the other. As a source of ground power these units had the usual maintenance problems attending the use of storage batteries.

In an effort to eliminate these maintenance problems, some operators coupled some of the generators developed for use on the aircraft engines to a small industrial engine and mounted the assembly on a cart, together with the necessary control, and used this as a source of ground power. These generators had been developed to run at a very high speed, too high for the usual industrial engine, and their shaft construction made the problem of coupling them to the engine very difficult. These units probably gave more trouble than the batteries.

Other operators converted conventional single operator arc welding generating sets, both motor and engine driven, from their usual constant current characteristics to a more or less constant voltage characteristic for this purpose. This was a makeshift operation since these generators were all designed for a much higher voltage than required and were equipped with differential compounding which was exactly the opposite of the compounding required for this purpose. These generators also had very high field currents and usually required two regulators in parallel in order to handle these field currents. The regulators rarely paralleled satisfactorily and quickly gave trouble.

Dry disk metallic oxide rectifiers such as the copper oxide, copper sulphide and selenium rectifiers have also been used as a source of the d-c power required. In general these units are much more bulky than the equivalent rotating generator. They also, of course, require a source of alternating current power supply which limits their portability. A rectifier must also have with it a transformer to step the line voltage down to the necessary rectifier input voltage, a saturated reactor or some equivalent means of controlling the voltage output of the rectifier, a d-c reactor to minimize the voltage ripple, as well as the necessary control and ventilation means.

The last two years have seen the rather general adoption of the rotating generator as a source of power for aircraft on the ground. The great majority of these generators are gasoline engine driven. The first units were built with a rating of about 4 kw but now the most popular size is a 15-kw unit. Other units are built in 5, 7.5, 30, 50 and 60 kw ratings. The great majority of these are 28.5-volt direct current. A few of them are dual voltage, that is, either 28.5 or 14 volts. Some of the units are motor driven and are portable for use in the hangar where electric power is available. Other units are motor driven and are arranged for location in a pit or tunnel beneath the apron of the airport. In these cases power is brought to the airplane on the ramp through a manhole or sunken access opening covered with a trap door.

A very few airplanes have been built with 400-cycle electric systems. Combination generating units supplying either 400 cycles or direct current have been built for airport servicing these types of planes together with the more standard d-c types. Some of the d-c airplanes have a few 400-cycle instruments on them and 400-cycle generating units are made to supply power for servicing these instruments.

A few 120-volt aircraft have been built but do not present any different problem in ground power equipment other than to require a generator of that voltage.

Most of the generating units are of the single-voltage 28.5-volt d-c type and the following discussion will largely refer to this type of set. Except for the smaller sizes (4 and 5 kw), the generators are usually separately excited, cumulatively compound. In general the exciters themselves are 120 volts so that they can in turn be used to supply power to floodlights when desired. The generators are all rated continuously at 50 degrees centigrade rise. The overload capacity of these generators depends on the type of prime mover driving them. The motor driven units, like most induction motor generator sets, will give 50 per cent overload readily. In order to keep the size, weight and first cost from being excessive, the engine driven units generally do not have such a high overload capacity.

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JOHN H. BLANKENBUEHLER is an engineer with both the Hobart Brothers Company and the Motor Generator Corporation, Troy, Ohio.

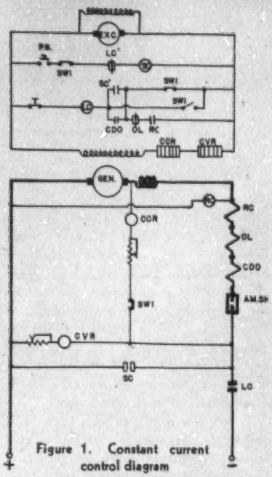

Figure 1. Constant current control diagram

They will usually give from 15 to 25 per cent overload. In a few cases 50 per cent overload can be obtained from them, but this is unusual.

All of these units are designed to use the standard carbon pile aircraft-type voltage regulator which is readily available at all aircraft service centers. These regulators will provide a voltage regulation of $\pm 1/2$ volt at 28.5 volts standard voltage. Some of the smaller units are arranged so that a throw of a switch or the reversal of the cable connecting the unit to the aircraft will operate to change the voltage supply to the aircraft from 28 to 14 or vice versa. Since it is standard practice to have one type of receptacle in the aircraft for 12 volts and another for 24 volts, automatic changeover of this voltage is readily feasible.

Although the constant voltage characteristic is by far the most popular, other characteristics have been developed to suit certain special applications. Since a constant voltage regulation in the generating unit does not mean a constant voltage in the aircraft because of the voltage drop in leads to the aircraft, systems have been developed to compensate for this lead voltage drop. Probably the simplest system is to connect the paralleling coil of the standard voltage regulator across some resistance in the load circuit of the generator, such as the commutating field coils and series field coils, and then to adjust the current in this paralleling coil so as to overcompound the regulator enough to compensate for the voltage drop in the leads going to the aircraft. From 4

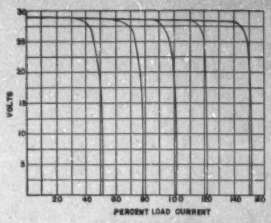

Figure 2. Characteristic curves for jet engine starting

to 5 volts overcompounding can be obtained in this way. This also enables the operator to use much smaller leads going from the unit to the aircraft and thus these leads will be more flexible and less burdensome.

The starting of jet engines seems to require another type of voltage current characteristic. The jet engine starting motor is a series motor and of course the torque delivered by it is therefore proportional to the square of the current delivered to the motor. If a constant voltage power source is used the initial inrush of current to this motor will be very high (2,500 to 3,000 amperes) and as the motor increases in speed this current will of course drop off. This means that the gears, bearings, and all of the parts of the motor starting system must be large enough to stand the initial inrush torque. If the input current to the starting motor could be limited at start to a definite value and then kept at this value throughout the starting cycle, the mechanical parts of the starting system could be much smaller and therefore weight would be saved. A generating unit for this purpose has been designed with which the operator can preset the current to be delivered to the motor. This is done as is shown in Figure 1, by using two voltage regulators, one in the normal voltage regulating connection and the other with its coil acting as a current coil instead of a voltage coil. Instead of putting the full generator current through this coil it is usually connected across the generator field coils as shown. At any current which will excite this coil to the preset value the carbon pile operated by this coil will act to regulate the field current so as to maintain this current constant. During this period the voltage will be lower than normal and the voltage regulator will have its pile in the minimum resistance condition. As soon as the voltage reaches the normal value, the voltage regulator will take control from the current regulator and maintain constant voltage. Voltage regulation curves such as are shown in Figure 2 are readily obtained with this control system. In practice it is also customary to have an automatic control system short circuit the generator, thus reducing its voltage to zero prior to closing the circuit to the aircraft. Immediately after the circuit to the aircraft is closed the contactor short circuiting the generator opens and current flows through the starting motor at the predetermined value. As the starting motor speeds up, the current is maintained constant up to the usual 28.5 volts. Then the current decreases and at a preset cutoff point, a current relay acts to disconnect the power unit from the starting motor.

Figure 3 shows the circuit diagram of a typical ground power unit. The apparatus shown includes the generator with its exciter, the carbon pile voltage regulator together with a transfer switch, and a manually operated field rheostat. By this means the operator can transfer to manual adjustment of the generator's terminal voltage in case of failure of the voltage regulator. This is purely for emergency operation. In some cases, such as the one shown, the carbon pile of the regulator is paralleled by a fixed resistor in order to adapt the standard regulator to the field

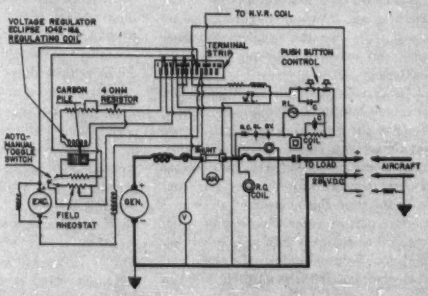

Figure 3. Typical ground power unit diagram

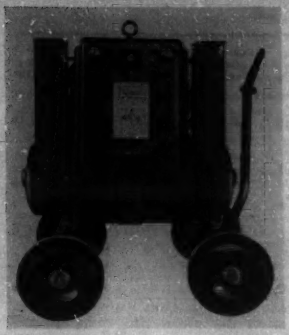

Figure 4. Motor generator set for hangar service

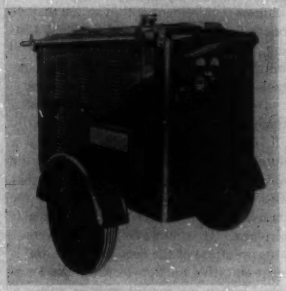

Figure 5. 200-ampere set on dolly-type running gear

currents being used. Better voltage response to changes in loads have been obtained with the regulator in the main field circuit than can be obtained with the regulator in the exciter field circuit. Figure 3 also shows the ammeter and voltmeter normally mounted on the control panel together with the contactor, in the output lead to the aircraft, which is operated by a push button. In some cases manually operated circuit breakers are used on the smaller units. All units are equipped with overvoltage, reverse current and overload relays. In those cases where the voltage regulator is compensated for lead drop the overvoltage relay must also be arranged in a similar manner. This is easily done by putting another coil on this relay connected across a resistance in the load circuit. In some cases the control of the line contactor is interlocked with a relay in the aircraft or with the plug inserted into the aircraft receptacle so that power may not be connected to the aircraft prior to proper insertion of this plug.

Engine driven equipment must also have the necessary controls for the engine on the control panel. These generally consist of a tachometer of the vibrating reed type, an engine hour meter to indicate when the engine should be overhauled or serviced, and the usual water temperature and oil pressure gauges together with an ignition switch and some sort of throttle control. In most cases the throttle is controlled by means of a tumbler switch on the panel through a solenoid in the engine compartment. In this manner the engine can be run at idling speed or at full governed speed.

In some cases overspeed relays are provided as a matter of extra precaution. The most simple overspeed relay is a voltage relay across the exciter which operates to open the ignition circuit of the engine if the exciter voltage rises to an excessive value.

The 4 and 5 kw units generally have batteries as well as the engine generators to provide high peak currents for a short time. These units are also equipped with a relay to disconnect the battery from the output line when the generator is not running so that the battery may not be discharged accidentally.

All of these generating units must be equipped with adequate radio interference suppression. This is generally accomplished with small grounding capacitors at various points. A 0.5 microfarad capacitor is connected from each output terminal to ground, a 0.25 microfarad capacitor is connected from each brushholder of the main generator to ground, and an 0.1 microfarad capacitor is connected from each brushholder of the exciter and from the regulator of the engine's battery charging generator as well as the ignition coil to ground. A 10,000-ohm suppressor is used in the lead from the ignition coil to the distributor and similar

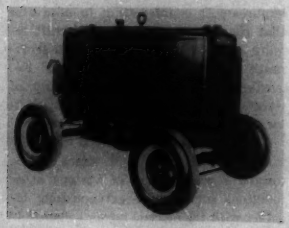

Figure 6 (left). 500-ampere set on trailer-type running gear

Figure 8 (right). 600-ampere self-propelled "PowerPull" unit

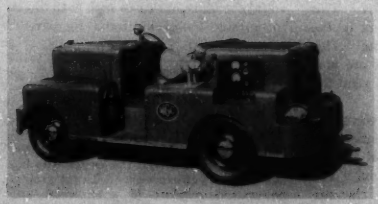

Figure 7 (left). 1000-ampere self-propelled "Powermobile"

Figure 9. (right) 2,000-ampere double generator unit mounted on a truck for mobility

Table I. D-C Ground Electric Loads in Amperes at 28½ volts

	Martin 202		Douglas DC-4		Douglas DC-6		Boeing 377 Cold		Boeing 377 Hot		Boeing 307		Consolidated Convair		Lockheed Constellation		Military	
	Cont.	Int.	Cont.	Int.	Cont.	Int.	Cont.	Int.	Cont.	Int.	Cont.	Int.	Cont.	Int.	Cont.	Int.	Cont	Int.
Heating and ventilating	109	109	121	121	174	174	215	215	120	120	17	17	3	19	279	279		18
Refrigeration									880	880								
Buffet		60		60	67	67												
Inverters	105	105	90	100	95	95	315	315	315	315			126	126	650	650	91	91
Radio	14	22	78	118	28	28	30	30	30	30	20	38	20	37	87	87	21	108
Instruments	4	5	10	10	17	17	6	6	6	6	14	14	14	14	55	55	3	3
Lights	33	33	70	70	185	185	60	60	60	60	40	40	53	53	80	80	2	2
Water heaters					30	30	44	44	44	44					86	86		
Booster pumps		20		15	20	20		15		15			50	50	50	50	7	7
Starters		198		198		198		240		240		183		175		175		276
Propeller feathering						200						147	6	153	20	170		
Baggage hoist																178		
Miscellaneous	6	12	6	10			20	40	20	40			7	7				
Total	271	564	375	702	616	1,014	690	985	1,475	1,750	91	439	279	634	1,307	1,810	124	505

suppressors are used at each spark plug. In a few cases completely shielded ignition harness is used.

The small sets use a duplex number 1/0 cable from the generator to the aircraft. Duplex cables are provided as large as 4/0. These cables are terminated in a model *AN-2551* plug for the 28.5 volt units. The 12-volt systems have an *R-3081* plug on them. Some of the 30-kw units and all of the 50- and 60-kw units use the *Cannon 16553* plug with from three to six number 3/0 cables per polarity. A plug of this type meant to carry 1,500 amperes with 30 feet of cable connected to it consisting of 12 conductors each 3/0 in size presents a formidable handling problem. The cable alone will weigh 250 pounds. For this reason, aluminum cable is being used in some instances. At the present time it is difficult to obtain a very flexible type of cable and its low conductivity does not bring about any considerable gain.

Interesting as are the electrical problems encountered in the development of this equipment, the mechanical problems become almost as varied and noteworthy. The motor driven generating sets for hangar use such as shown in Figure 4, for instance, have all of the controls arranged so that any contacts which might create a spark are more than four feet from the floor. Likewise the air intakes at the end of the set are equipped with a duct raising the actual air intake to more than four feet above the floor. This is done to prevent the explosion of any vapors which might collect on the floor of an aircraft hangar. This unit as can be seen is also equipped with a special running gear which prevents turning of the wheels unless the tongue or draw bar is depressed.

Figure 5 shows one of the smaller 5-kw units which as noted before has the batteries on it to provide high currents for short times. This unit is mounted on a 3-wheel dolly and can be towed or pushed to its destination. Figure 6 shows the most popular 15-kw unit on a 4-wheel running gear for towing. Such a running gear is equipped with parking brakes and the unit also has large compartments at the sides for storage of tools and cable. Figure 7 shows one of the self-propelled units in which the engine may be used to either drive the generator or propel the entire unit wherever desired. This unit may also be used for towing the smaller planes requiring up to 3,000 pounds draw-bar pull. Figure 8 shows a smaller variety of this self-propelled unit. Both of these types are developed with a great variety of accessories such as floodlights, fire extinguishers, and so forth.

Figure 9 shows a 60-kw unit consisting of two 30-kw engine driven generating sets arranged for either separate or parallel operation. This unit is equipped with one *AN-2551* plug with 20 feet of cable and one *Cannon 16553* plug with 30 feet of 12-conductor cable on each side. Storage of this cable in an accessible position became a real problem here. A truck of this sort may also be used to tow rather large aircraft requiring a draw bar pull of 8,000 pounds or less. This unit is also equipped with a carbon dioxide fire extinguishing system for connection to the plane.

Two 50 pound CO_2 cylinders are manifolded to a hose reel on the truck. The hose from the reel is plugged into the aircraft and piped through the CO_2 distribution system in the aircraft. This avoids discharging the cylinders in the aircraft in case of a ground fire. This unit and all of the other units are normally provided with one or several 2.5 pounds CO_2 hand extinguishers.

Table I shows the ground power requirements of the various airplanes now most commonly used, expressed in amperes for the 28.5-volt system. The tabulation indicates roughly the apparatus using the current and the current requirements are divided into two classes, continuous and intermittent. A continuous requirement is that which may be on during the warm-up, functional testing, and passenger loading. The intermittent loads are those on for a few seconds to a few minutes during this time.

Table I is an oversimplification of a terrific mass of data kindly given me by all of the aircraft manufacturers and a number of the operators. Because of the different equipment used by the different airlines it is probable that none of the data is exactly correct for any particular airplane. It is, however, a good average interpretation of the data available and is representative enough to show the loads which are being encountered in present-day operations. It is interesting to notice that in general the more recently the plane was developed the more electric power it requires. It is interesting to note also that the modern long-range aircraft require 30 to 50 kw and the modern short-range aircraft require 8 to 15 kw. Regardless of these indications many of these aircraft are being serviced with 5-kw generating sets and battery carts.

The trend at the present is definitely toward larger units and seems to be toward a combination of various other functions such as fire extinguishing together with the electric power generation on one self-propelled unit. Newer units are arranged to provide more than one voltage and in some cases with both direct current and 400-cycle alternating current.

Discussion

Stanley Taborsky (Glenn L. Martin Company, Baltimore, Md.): The author's paper is very interesting, and the writer who has had ten years of experience with the ground power requirements of commercial and

military airplanes, wishes to supplement this information with additional data which may be of interest.

As Blankenbuehler explained, portable 200-ampere hour battery carts were in extensive use prior to the recent conflict and were quite capable of starting engines and supplying sufficient power for the limited amount of preflight test required. Since that time the very large increase in electric loads in aircraft with associated power plants of unprecedented power made it necessary to provide external ground power equipment of larger capacity. Our first experience was with metallic rectifiers of the magnesium-copper-sulphide type and others. These units have excellent engine starting characteristics but very poor voltage regulation. In addition, inclement weather conditions have a deleterious effect on the rectifier elements which make it necessary to frequently replace the rectifier stacks. It was therefore decided to use motor-driven generators for this work, and since equipment of this type with the desired operating characteristics was not commercially available, a standard 200-ampere motor-driven arc welding machine was modified for this purpose with a voltage regulator, reverse-current relay, circuit protection, and remote control.

This unit was standardized and the armed services found it satisfactory for their activities in this country and abroad, for use on medium size airplanes such as the *PBM*, *A30* and *B26*, until commercial equipment became available.

External ground power equipment is used by airplane manufacturers for the following functions:

1. Engine starting.
2. Functional tests of the electric system, including the adjustments of limit switches on electric motor-driven trim tabs, cowl flaps and oil cooler flaps.
3. Operation of radio, radar, and other electronic equipment.
4. Occasional charging of the ship's batteries after installation.

To satisfactorily accomplish these functions it was found necessary to provide equipment to meet the following general specifications:

1. Sufficient capacity to start engines in a minimum amount of time in order to prevent engine starter burnouts. As is known, these starters are made for a very low duty cycle in order to conserve weight and prolonged operation is therefore objectionable. In addition, the equipment must maintain a voltage level above the dropout voltage of the starter relay on the airplane during engine starting to prevent chattering and subsequent failure of the relay.
2. Voltage regulation (±2 per cent) comparable to the electric system on the airplane so that mechanical controls can be properly adjusted and tested to insure flight integrity.
3. Minimum noise level and ripple not to exceed 1 per cent for radio, radar, and electronic tests.
4. Reverse-current control for use in charging ship's batteries.

Our experience in these problems has dictated that commercial aircraft and small military airplanes should be serviced with external ground power equipment having a continuous rating of 300 amperes at 28.0 volts with a 15 seconds overload rating of 600 amperes at not less than 18 volts and a 2 minutes overload rating of 400 amperes at not less than 24 volts direct current.

Heavy military airplanes require the use of equipment with a continuous rating of 600 amperes at 28.0 volts with a 45 seconds overload rating of 2,100 amperes at not less than 18 volts, and a 2 minute overload rating of 1,000 amperes at not less than 24 volts.

Additional desired features are remote control, over-voltage protection, line drop compensation, and overload protection. With regard to over-voltage protection, I wish to point out that voltage regulators of the carbon-stack type have been known to stick thereby causing excessive voltage and considerable damage to such equipment as storage batteries, radio equipment, and lighting circuits on the airplane. Separate generator field exciters are necessary to provide quick voltage recovery during shock loads and the use of auxiliary batteries to supplement this equipment is objectionable from a maintenance point of view. The units must be rainproof and portable, with provisions for towing with a truck. Self-propelled units are also desirable in some activities. Since it is not feasible to provide electric power outlets in all locations for the operation of this equipment, it is necessary to provide gasoline driven units for use in remote locations.

Jet engines must be started as quickly as possible since prolonged ground operations curtail flight time due to the large fuel consumption of the engines and the small capacity of the fuel tanks. The 600-ampere equipment used for heavy military airplanes is therefore operated in parallel to provide the capacity to start jet engines every 15 seconds on multiple-engine ships. It usually takes about 45 seconds before a jet engine can be cranked to the firing speed, at which time the starter is de-energized. However, at the end of 15 seconds, the inrush current has diminished to such a value that the next engine can be started without overloading the two external power units operating in parallel. This procedure is continued until all engines have been brought up to firing speed with the equipment installed at the run-way to allow for immediate take-off.

We have had no experience with the constant current method of starting airplane engines. It is, however, understood that the Services will specify this type of equipment for future applications at which time operating data on its use will be presented.

The writer sincerely hopes that this information, together with Blankenbuehler's excellent paper, will stimulate manufacturers' interest and result in the production of standard equipment capable of satisfying the requirements of airlines and aircraft manufacturers.

Rural Distribution Voltages

WADE M. EDMUNDS
MEMBER AIEE

GLENN B. ROLOSON
NONMEMBER AIEE

AT THE TIME the Rural Electrification Administration (REA) was established by executive order in May 1935, the voltages on rural distribution systems covered a wide range and included some of the multigrounded Y systems that are in use today. Single bushing 6,900-, 7,200-, and 7,620-volt transformers had been made available by the manufacturers.

The REA made an extensive study of the voltages that were then in use and in December 1936, issued suggested specifications for 12,000/6,900 volt Y-multigrounded neutral distribution circuits. This selection was based largely upon Tennessee Valley Authority practice and the fact that transformers were available. Within a short time the REA's suggestions were changed to the present 12,470/7,200-volt Y-multigrounded neutral specifications.

After lines had been constructed to serve a definite area, there was a great demand for service outside that area with the result that many additions were made which had to be served over the original lines or modifications of them.

The earlier REA-financed systems had consumptions on the order of an average of 50 kilowatt-hours per month per consumer. Up until the last few years most lines were designed on the basis of approximately 100 kilowatt-hours per month per consumer. However, the consumption per consumer has been steadily increasing until now consumptions of 300 and 400 kilowatt-hours per month per consumer are not unusual and the end is not yet.

The system operators have had to meet this load growth by installing voltage regulators, increasing the number of phases, and increasing the size of conductor. There is a limit to the number of regulators that can be cascaded and the amount of rephasing that can be done. There is also an economic limit to increasing the size of conductor. Increasing the size of conductor reduces the resistance but reduces the inductance only a little. For lines with reduced size neutral, the curve of admittance plotted against pounds of conductor begins to flatten out above number 2 copper conductivity phase wire. With some conductors, the cost per mho for 4/0 conductor is more than double that of number 6.

There is an increasing demand for service in sparsely settled areas where the distances to be covered are much greater than on systems heretofore constructed. More than usual economy is required to construct these systems.

It appears that some departure from the present standard distribution voltage is required. As early as 1937 the REA considered the possibility of using 22,300/-13,200 volts as another multigrounded Y voltage standard and the idea of using this or a slightly higher voltage has come up from time to time.

The load capacity of a distribution system varies as the square of the system voltage. A 23.9/13.8 kv Y-connected multigrounded system would have 3.67 times the capacity of a 12.47/7.2 kv system using conductors of the same size. A uniformly loaded line at 23.9/13.8 kv may be extended 90 per cent further and serve 3.67 times the area of a uniformly loaded line at 12.47/7.2 kv with the same size conductors. This takes into account the loads on the additional 90 per cent of line.

About 1942 a co-operative in Minnesota purchased approximately 50 miles of 23.9/13.8-kv Y-connected multigrounded transmission line with some 13.8-kv transformers (15-kv insulation class) transformers installed along the line. Most of these transformers had been installed in 1916. After the acquisition of this line, the co-operative began buying transformers to be used on extensions to serve farmers and in 1944 there were some 150 or 160 13.8-kv transformers (15-kv insulation class without lightning arresters) connected on this line and the manager stated in 1944 that in the two years the co-operative operated the line, not a single transformer had been lost due to lightning and that the operating experience with the transformers had been excellent. An REA field construction engineer reported on this line in April 1947 and from what he could learn, no more 13.8-kv transformer failures occur than with the 7.2-kv transformers on other parts of the co-operative's system. Some of the 23.9/13.8-kv line has been absorbed into the 12.47/-7.2-kv portion of the co-operative's system so that there are now 80 miles of 23.9/13.8-kv line with 70 transformers.

The estimated costs of two proposed projects in Colorado are shown in Table I.

Increasing the distribution voltage to

Table I

	Project A	Project B
Primary miles	387	208
Consumers	434	238
Cost using 12.47–7.2 kv	$702,672	
Cost with transmission line and substations added	$559,756	$625,000
Cost with 23.9–13.8-kv distribution*	$475,565	$415,000
Per cent saving 23.9–13.8 kv over 12.47–7.2 kv plus transmission	15	34

* 15-kv insulation class transformers with the same lightning arresters as if the transformers were connected in delta.

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WADE M. EDMUNDS and GLENN B. ROLOSON are both with the engineering division of the Rural Electrification Administration, Washington, D. C.

23.9/13.8 kv does not materially affect conductor separation although it would decrease the maximum allowable span length to a small extent. Apparently the National Electrical Safety Code permits the same ground clearance for single-phase sections of 23.9/13.8-kv systems as on present systems but, however, an additional 2-foot ground clearance is required on multiphase sections of 23.9/13.8-kv systems.

No REA-financed systems have been completed as yet using 23.9/13.8-kv distribution. The transformer manufacturers do not recommend nor guarantee their 15-kv insulation class transformers for this service. Their objections are based mainly on these two points:

1. On a 13,800-volt 15-kv insulation class transformer the insulation between the winding and the case was designed for the electrostatic voltage of 7,968 volts. The phase voltage of 13,800 would occur only between the winding and case under faulted conditions. When this transformer is connected between a phase wire and ground on a 23.9/13.8-kv Y-connected system, the voltage between the winding and case always would be 13,800 volts.

2. On a 13,800 volt 15-kv insulation class transformer connected between a phase wire and ground on a 23.9/13.8-kv Y-connected system, a fault to ground on one phase would cause an excessive voltage rise on the other phases which would damage the transformers.

The effect of line-to-ground faults on the voltage of the unfaulted phases was calculated by the methods given in the Electrical Transmission and Distribution Reference Book. The calculations were only for the substation and the distribution line without the transformers. According to these calculations, the voltage rise for number 6 conductor with the fault at a considerable distance from the substation was approximately 58 per cent. It is realized that the calculations were for lines which are not comparable to distribution lines built to REA specifications, but it is likely that calculations of this kind have tended to create the impression that the effect of the neutral shift is greater than is actually the case.

In order to determine the voltage rise due to neutral shift, it was decided to place line-to-ground faults on one phase and measure the voltage on the unfaulted phases on an existing system in an area of high ground resistivity. The system selected for the tests was that of the Southside Electric Cooperative, in the vicinity of Blackstone, Va.

A 50-ampere oil circuit recloser with the current coil short-circuited out was used in making the line-to-ground faults. A type *PM 13*, 6-element oscillograph was used to record the voltage on the unfaulted phases and the fault current. Ground resistances were measured with a model *251* Vibroground.

The oil circuit breaker was connected between a phase wire and the neutral which was connected to a ground rod. A current transformer was placed in the lead from the oil circuit breaker to the neutral. Potential transformers were connected between phase wires and neutral. The fault current reading was obtained where the fault and oscillograph were in the same location but in some instances it was too large to get a reading.

The first series of tests was made on lines feeding from a 66/12.47/7.2-kv 5,000-kva substation. In these tests the resistance of the neutral to ground ranged from 22 to 24 ohms at the location of the tests. Oil circuit reclosers in the distribution circuits limited the duration of the faults.

In test *A*, the fault was made at a distance of 11.9 miles from the substation. This mileage consisted of 5.9 miles of 3-phase, 1/0 copper-phase wires with number 6 neutral and 6.0 miles of 3-phase, number 4 phase wires with number 6 neutral. The recorded voltage rise on the unfaulted phases at the location of the fault was 14 and 18 per cent and lasted a maximum of 15 cycles.

In test *B*, the fault was made at a distance of 26.1 miles from the substation. This mileage consisted of 5.9 miles of 3-phase, 1/0 copper-phase wires with number 6 neutral, 9.6 miles of 3-phase number 4 phase wires with number 6 neutral and 10.6 miles of V-phase* number 6 phase wires with number 6 neutral. The fault current was 120 amperes, and the recorded voltage rise on the unfaulted phase at the location of the fault was 23.5 per cent and lasted a maximum of 38 cycles.

In test *C*, the fault was made on a single-phase tap at a distance of 22.1 miles from the substation. This mileage consisted of 5.9 miles of 3-phase 1/0 copper-phase wires with number 6 neutral, 9.6 miles of 3-phase number 4 phase wires with number 6 neutral and 6.6 miles of single-phase number 6 phase wire and number 6 neutral. The recorded voltage rise on the unfaulted phases at a point 10.6 miles on the V-phase number 6 phase wires with number 6 neutral line beyond where the single-phase tap left the main line, was 7 and 20 per cent respectively, which lasted a maximum of 7 cycles.

In test *D*, the fault was made at a distance of 11.9 miles from the substation. This mileage consisted of 5.9 miles of 3-phase, 1/0-copper-phase wires with number 6 neutral and 6.0 miles of 3-phase number 4 phase wires with number 6 neutral. The voltage was recorded at a point beyond the fault and at a distance of 26.1 miles from the substation. The line beyond the fault to the point where the voltage was recorded consisted of 9.6 miles of 3-phase number 4 phase wires with number 6 neutral and 10.6 miles of V-phase, number 6 phase wires with number 6 neutral. The recorded voltage rise was 12.5 and 30 per cent respectively, and lasted a maximum of 18 cycles.

The second series of tests was made in an area about 90 miles from the first series of tests on lines feeding from 13.2/-12.47/7.2-kv 600-kva substation. In these tests the resistance of the neutral to ground was 25 ohms at the location of the tests.

In test *E*, the fault was made at a distance of 12 miles from the substation. This mileage consisted of 3-phase number 6 phase wires with number 6 neutral. The fault current was 86 amperes, and the recorded voltage rise on the unfaulted phases at the location of the fault was zero per cent on both phases. Actually, a slight decrease in voltage was shown.

In test *F*, the fault was at a distance of 20 miles from the substation. This mileage consisted of 18 miles of 3-phase number 6 phase wires with number 6 neutral and two miles of V-phase number 6 phase wires with number 6 neutral. The voltage was recorded at a point 12 miles from the substation on the 3-phase section of the line. The voltage rise on the unfaulted phase was 0 per cent. Actually, a slight decrease on voltage was shown.

In the first series of tests the voltage rise on the unfaulted phases, due to a line-to-ground fault, ranged from seven per cent to a maximum of 30 per cent. The effect of the fault current on the regulation of the 66-kv line supplying the delta connected, 5,000-kva substation was probably negligible so that practically the full effect of the neutral shift was apparent. Also, the *IZ* drop in the neutral was larger than would have been the case had the neutral been the same size as the phase conductors.

In the second series of tests there was no voltage rise on the unfaulted conductor due to a line-to-ground fault. This can probably be explained by the fact that the power is supplied to the delta-connected 600-kva substation over a 13,200 volt line and that the regulation,

* V-phase refers to open Y circuit.

due to the fault current, cancels out the voltage rise produced by the neutral shift. Also, the IZ drop in the neutral was not as large as would have been the case had the neutral been of less size than the phase conductors.

The foregoing tests were made in areas of comparatively high ground resistance. These tests did not show as much neutral shift as had been calculated for bare lines without taking into account the constants of the system supplying power to the cooperative's substation.

Weighing the need in some areas for a considerably higher distribution voltage that would be economical against the possible hazards of equipment failure, it has been decided that some construction at a distribution voltage of 23.9/13.8 kv is warranted at this time. Two projects have now been approved for this type of construction, one in Arizona and one in New Mexico.

The Arizona project will consist of 284 miles of distribution line to serve 365 consumers. The distribution voltage is 23.9/13.8 kv. The first section, 125 miles to serve 174 consumers, is under construction. The area farthest from the source of power is at a distance of 70 miles. The distribution transformers to be used on this system are 13,800-volt transformers of the 15-kv insulation class.

The project in New Mexico will consist of 166 miles of distribution line to serve 248 consumers. The farthest extremity of the project from the substation is 76 miles.

Consideration is being given to a new distribution transformer design which will have insulation somewhat higher than the 15-kv class.

The performance of the two types of transformers will be watched closely after construction is completed, and tests made to determine which is more satisfactory.

Conclusion

A higher distribution voltage for use in rural areas is definitely needed where farms are widely separated and kilowatt-hour consumption will be substantial. Use of the 23.9/13.8-kv system is, of course, nothing new but in order that construction costs be within limits of feasibility, a substitute for the presently available 25-kv class distribution transformer must be found. A voltage rise of 30 per cent during a line-to-ground fault, lasting 18 cycles, does not appear excessive, and it is believed worthwhile to install 15-kv class transformers on an experimental basis.

Reference

1. Electric Transmission and Distribution Reference Book. Westinghouse Electric Corporation, East Pittsburgh, Pa. Chapter 15, 1942.

Discussion

W. A. Sumner (Westinghouse Electric Corporation, Sharon, Pa.): The paper by Edmunds and Roloson indicates that there is still a field for pioneering work in rural electrification. In order to produce transformers correctly designed for this new and sparsely settled area where the present voltage appears to be inadequate, the manufacturers need as much factual data as possible. The tests and applications reported in this paper, while limited in number, provide a contribution to this subject and deserve consideration.

The authors are correct in stating that "more than usual economy is required to construct these systems." This includes, as a considerable portion of the first cost, the distribution transformers. While the investment in transformers must be kept as low as possible, it is also essential that their design be adequate for these more exposed areas so that the cost of operation and maintenance is held within economical limits.

In attempting to select a transformer for this service, the authors have explored the possibilities of using existing transformers. Here they are confronted with the fact that the present 13.8-kv (15-kv class) distribution transformers are not designed for this service, and, therefore, cannot be guaranteed for it by the manufacturers. On the other hand, if these transformers are designed on existing 25-kv parts, which are, generally speaking, of the small power class, the cost becomes prohibitive. What is needed is a new transformer designed for this specific purpose.

Care should be used in the interpretation of the tests reported in the paper. While the maximum overvoltage developed in these tests cannot be considered an excessive value, it should be realized that this materially affects the design of the protective devices for the transformer as they will be called upon to operate and restore line voltage conditions when the voltage is 130 per cent of normal 13.8-kv line voltage.

As to evaluating the prolonged and apparently successful performance of the 13.8-kv (15-kv class) transformers of the 1916 vintage, care should be used as this experience does not agree with that of some others who have tried this type of application. In the absence of detailed data, the insulation design of these units remains an unknown factor. The actual insulation design used obviously is a very important point in an analysis of this kind. These units were probably equipped with relatively small bushings in keeping with the general practice of that time. With the low flashover characteristics of these bushings, it is to be expected that transformers would receive relatively good lightning protection at the expense of an abnormal number of fuse outages. This is the usual experience, and it is felt that the data on outages should be included to enable complete evaluation of the performance of these transformers. The proposed project, involving 13.8 (15-kv class) transformers of current design, will provide much valuable field information, but it will take time to secure sufficient data to judge the life expectancy of such designs under these conditions.

In all these cases, the apparently successful operation under the limited conditions enumerated is made possible by the safety factors used in the design. In selecting these values, the manufacturers take into account, among other things, those elements necessary to produce a satisfactory length of life, and we do not feel that they are excessive when one considers the length of life which is normally expected of distribution transformers. In any event, these factors are not matters for standardization and are subject to possible change as manufacturing experience and field data are accumulated.

A more rational approach, in which a specification is drawn up based upon the operating needs for this specific application, seems desirable. We have, in fact, been asked by some of the utilities to furnish a transformer of this nature for a similar application, and we have supplied for a number of years, for a special industrial application, a transformer which has an insulation level intermediate between the 15-kv and 25-kv classes. Field experience with these transformers, covering some 18 years, has been very successful.

While this meeting is not a suitable occasion for drawing up specifications, it is suggested that the solution to this problem be worked out in a manner similar to that of the present successful 7,200-volt rural transformers. In the interests of standardization, it is proposed that consideration be given to rating the new transformer at 25,000 grounded Y/14,400-volts, using the existing EEI-NEMA standard high-voltage rating of 14,400/13,800/13,200/12,870 (12,540 reduced capacity). As to kilovolt-ampere ratings, it is not economically feasible to build a unit smaller than 5 kva at this higher voltage. As an approach to the problem of establishing the insulation test, it is recommended that considerations similar to those used in setting the test for the present 12,470 grounded Y/7,200-volt rural transformer be followed. In these transformers, the applied voltage test is omitted and an induced potential test of 26 kv is specified, this test to be made with the neutral grounded. This test places a stress to ground on the insulated end of the winding of 26,000 compared with 34,000 volts on the standard 2-bushing transformer. This suggests that these units should be designed for an induced potential test of approximately 40 kv. The resulting stress to ground on the insulated end of the winding of 40,000 volts compares with 50 kv on the present 25-kv class standard distribution transformers. These transformers should be designed to take full advantage of the contemplated multigrounded service, including the use of only one high-voltage bushing.

and with the other high-voltage line lead and the low-voltage neutral permanently grounded to the case, and should make use of existing highly tooled rural transformer parts insofar as feasible.

Harry N. Stafford (Rural Electrification Administration, Washington, D. C.): Since the writing of the paper one RRA-financed system using the 23.9/13.8-kv distribution voltage has been energized. This system is owned by Navopache Electric Cooperative, Inc., and is located in the eastern portion of Arizona in a mountainous region at a mean altitude of 7,500-foot elevation. When this system was first conceived, it found that it would not be possible to use the standard 12.5/7.2-kv distribution because of the long distances involved, nor was it possible to justify economically, the use of transmission lines because of relatively low consumer density. Hence it was decided to use a higher distribution voltage. This system has only been in service a few months and some refinements in design are anticipated when we have had more operating experience. There are presently 130 distribution transformers of the 15-kv insulation class installed and no failures of these transformers have occurred, although the system has been exposed to a portion of a very severe lightning season.

Steps have been taken to reduce the anticipated neutral shift and accompanying high voltages to a minimum under fault conditions. Every pole is provided with a ground tied into the system neutral which has resulted in a resistance of neutral to ground of approximately 4 ohms which is considerably lower than the 22 to 24 ohms measured in the test made on the Virginia system as described in the paper. Special attention has been given to substation grounds and neutral connections in order to reduce neutral resistance to a minimum. It is the observation of the writer after conducting preliminary tests on this 23.9/-13.8-kv system that little or no trouble is anticipated from neutral shift phenomenon.

The following is the voltage rating of equipment and devices used on this system:

1. Pin-type insulators, 22-kv class—95 kv dry flashover, 65-kv wet flashover.
2. Sectionalizing fuse—25-kv rating.
3. Distribution transformers up to 50 kva—15-kv insulation level.
4. Transformers 50 kva and over, including substation transformers—25-kv insulation level, minimum.
5. Lightning arresters, 15-kv rating, expulsion type.

One of the principal limitations of this type system is that there is currently no oil circuit recloser available for use on the 3-phase line which is comparable in cost to those used on existing REA systems of the standard 12.5/7.2-kv voltage.

R. F. Quinn (General Electric Company, Schenectady, N. Y.): The material in the authors' paper is valuable to those interested in rural power systems because both the tests and the cost figures add two important items to the store of information available to the system designer.

It is felt that the cost figures shown in Table I of the author's paper merit the following observations.

Project A

The figures under project "A" indicate that total cost serving the load with 12.47/-7.2-kv distribution and multiple substations and transmission is lower than total cost serving the load with one supply point and 12.47/7.2-kv distribution. This same trend is indicated by a similar economic study made less than a year ago.[1] This latter study, however, was based on greater load areas.

It is interesting to note that the authors show a lower cost for serving the same load area with 23.9/13.8-kv distribution from one source. It is probable that the cost is lower in this case because transformers and lightning arresters of substandard insulation class are used.

Project B

The figures under project "B" also show a lower cost using 23.9/13.8-kv distribution than using multiple substations and 12.47/-7.2-kv distribution. However, no figure is given for the case of one source and 12.47/-7.2-kv distribution. A figure for this latter condition would be interesting and would complete the picture.

Projects A and B

The comparative figures between projects "A" and "B" illustrate a broad principle of system economics as follows. It is fundamental that as a system grows in load density, area, or both, a given distribution voltage becomes outgrown and it becomes necessary to provide transmission or subtransmission and thereby break the area up into smaller units in order to use the same distribution voltage.

Therefore, as the load and distance increase, the system using transmission and substations should look better compared to higher voltage distribution. The per cent savings indicated for projects "A" and "B" bear out this principle very well. That is, project "A" having greater load and distance shows less advantage for higher voltage distribution than project "B".

If the load and distances were to increase in project "A", this saving in favor of 23.9/-13.8-kv distribution would vanish. As load continued to grow the saving could be in favor of subtransmission and substations and 12.47/7.2-kv distribution.

System Design and Continuity

A very important consideration that must always be factored in distribution system design is continuity of service. From the viewpoint of continuity of service, feeders must be kept reasonably short and multiple sources of supply must be provided. From a practical operating standpoint, many engineers who have studied rural systems feel that feeders should not be longer than 25 or 30 miles and that substations should generally not be larger than 1,000 to 1,500 kva each.

Adequate voltage conditions and reasonable losses are easily obtained using these feeder lengths and these substation sizes. Therefore, if continuity and general system operating considerations indicate these feeder and substation limitations, and if with these limitations, voltage and loss conditions are satisfactory using 12.47/7.2-kv distribution, there appears to be no requirement for using higher distribution voltage for the general case.

REFERENCE

1. WHAT SIZE SUBSTATIONS FOR ECONOMICAL RURAL POWER SYSTEMS?, R. F. Quinn, N. M. Neagle. *Distribution Magazine*, General Electric Company (Schenectady, N. Y.), January 1948.

E. A. Link (Rural Transformer and Equipment Company, Milwaukee, Wis.): Edmunds and Roloson are to be complimented on their paper for presenting progressive thinking on higher distribution voltages for rural lines. Much is to be said in favor of higher distribution voltages, both from the standpoint of wider consumer coverage and increased loads. In Europe, the economics of higher distribution voltages have been definitely proved in rural areas and these higher voltages have been used for many years. Almost conversely, the same problem in Canada, where the supplying of loads to customers spaced two per mile has been solved by limiting the amount of load demand supplied to each customer. Devices are used in Canada to relieve the system of load that exceeds approximately 7 kva demand per customer. In this way, reasonable sizes of conductors and reasonable numbers of regulators keep the cost per mile within economic limits.

It is our feeling that since the problems of load growth in the United States are already upon us in that in certain areas, definite steps must be taken to relieve the situation. We feel sure that the American public will not be content to live under a limited peak demand of power consumption and that our thinking must now be projected in a manner described by the authors.

The load growth on existing rural lines has exceeded all expectations. From a service standpoint, we have been going around in circles, trying to keep pace with the growing demands of the rural consumer. Problems of voltage regulation and system capacity are common problems to our co-operatives. Higher distribution voltage is a logical answer to many of these problems. As the paper points out, good economics can be realized in going to higher voltages on systems now requiring increased conductors or regulators brought about by increased load growth. The problem of converting from a normal 12,470-volt system to a 23,900-volt system may relegate the use of 23,900-volt distribution to new areas but it is also possible to gradually make this conversion by running a dual voltage system with autotransformer tie-ins. It seems quite reasonable that new systems in low load concentration areas should be laid out with consideration to the 23,900-volt distribution with the possibility of justifying the addition of the remote and hard to serve customers.

As transformer manufacturers, we believe that the obstacles of insulation pointed out by the writers can be overcome. In fact, the margin of safety of insulation brought about by the use of improved materials and design has been steadily increased over

the last few years. Actual test indicates that present 15-kv class transformers are much closer to 25-kv class and in addition, impulse breakdown levels of lightning arresters are now much lower than earlier devices, thereby giving a very comfortable margin of safety which could be extended to the 25-kv class.

Another major problem in the use of 23,900-volt distribution is overload protection and isolation of the individual transformer. Recent developments have opened a new field in the overload protection of transformers so that this may now be accomplished on the primary side of the transformer. Conventional fuses previously used for primary protection of transformers had time current characteristics that would not follow the heating characteristics of the transformer. These new overload devices, however, will accurately follow the overload heating capacity of the transformer, as well as co-ordinate with secondary overload devices and primary sectionalizing devices.

The over-all object of 23,900-volt distribution is cost savings to the ultimate user. It is quite possible that the reasoning used in the article under discussion will effect savings not only in the sparsely settled areas but also in the areas loaded beyond the designed line capacity.

The use of 23,900-volt distribution offers a new challenge to operators, as well as manufacturers, and a satisfactory answer is almost sure to be found by close co-operation between both parties.

J. C. Cunningham (Westinghouse Electric Corporation, Pittsburgh, Pa.): This discussion will be confined to the voltages which occur during faults and their effect on the application of protective devices.

The authors calculated 58 per cent voltage rise for a line with 6A conductors by methods given in chapter 15 of the Westinghouse Electrical Transmission and Distribution Reference Book.[1] Without more information on the line I cannot check their figure exactly but it appears that they must have used the curves in this chapter which include the effect of zero-sequence resistance but neglect positive and negative sequence resistance. For a circuit using 6A conductors R_1 and R_2 are not negligible because the resistance is approximately three times the reactance. However, Table 2 of chapter 15 gives formulas which include all resistances. Based on these formulas a line of REA configuration with several miles of 6A phase and neutral conductors and 2,500 meter ohms ground resistivity would experience 26.5 per cent voltage rise during a single-line-to-ground fault instead of the 58 per cent mentioned in the paper.

Rigorous calculation of fault voltages on a multigrounded 4-wire circuit is quite laborious although a relatively accurate workable method has been developed.[2] For the purpose of applying apparatus to rural distribution circuits the formulas in Table 2 of chapter 15 are probably adequate even though they assume transposition of the line (contrary to REA practice).

Following the tests which REA conducted at the Southside Electric Cooperative we prepared a report which compared these test results with values we calculated by a

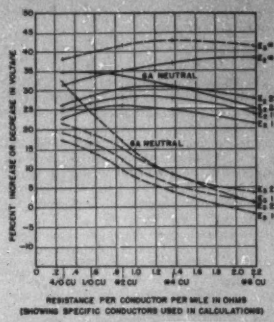

Figure 1. Voltage to neutral of phase 2 and 3 caused by fault between phase 1 and neutral on multigrounded circuit

The numbers following the voltage designation indicate ground resistivity in meter ohms. The neutral conductor is two sizes smaller than the phase conductors indicated by the abscissa except for the curves marked 6A neutral

Phase rotation is 1-2-3

method which assumes that the resistance of connections between neutral and ground is zero. Even though the local ground resistances were quite appreciable, the test values of voltage checked very closely with the calculated values. Based on this close agreement we concluded that voltages on multigrounded REA circuits could be estimated with reasonable accuracy on this basis, if the fault were at least several miles away from the substation.

In order to obtain a more general picture of fault voltage magnitudes the curves of Figure 1 were plotted.

They apply only where the following assumptions are valid:

1. The source of power is grounded and its impedance is negligible by comparison with line impedance to the point of fault.
2. The fault is far enough from the source that the resistance between neutral conductor and ground, and other end effects can be neglected.
3. Ground resistivity is uniform.
4. Conductor size is uniform between the fault and the source.
5. The neutral conductor is two wire sizes smaller than the phase conductor (except for the curves marked 6A neutral).
6. The line configuration is per drawing $C1_{5/4}$

The number of circuit conditions covered by the calculations and tests described before is insufficient to draw final conclusions. However, it appears certain that voltages between phase and neutral conductor may frequently reach 130 per cent of the substation voltage and occasionally approach 140 per cent. An allowance must also be made for increase in the substation voltage above normal at light load. All in all it appears that voltages of 18–19 kv might be encountered frequently and even 20 kv might be exceeded occasionally on a 13.8/23.9-kv multigrounded system. It is significant that the ratio 20/23.9 kv corresponds almost exactly to the usual ratio of lightning arrester rating to line voltage for effectively grounded systems.

Lightning arresters are normally selected on the basis of the maximum phase-to-neutral voltage which may occur during any kind of fault. This is because of the possibility that a lightning arrester may discharge on one phase during a solid ground on another phase and thereby encounter recovery voltage corresponding to the fault condition. It must be recognized that a fault will not occur every time a lightning arrester discharges and that not every fault will cause the maximum fault voltage.

In addition lightning arrester performance will vary considerably with the severity of lightning exposure, system recovery voltage characteristics, and manufacturing variation in the lightning arresters themselves. For these reasons 15-kv lightning arresters may not have an excessive failure rate in some 13.8/23.9-kv installations but may be unsatisfactory in others. One must also consider that loss of transformer surge protection is probably more serious than the loss of the lightning arresters themselves. This would be especially serious in the low-density load areas for which these systems are contemplated because of greater exposure and the high cost of patrolling.

If 13.8/23.9-kv distribution is going to be needed frequently, careful consideration should be given to the development of a new distribution-class 20-kv lightning arrester, to be co-ordinated with the impulse strength of the special 25-kv class single-bushing transformers suggested by Sumner. This combination would offer an attractive solution to the technical problem, but the activity would have to be reasonably high to produce apparatus which would be economically attractive.

On the basis of the Southside Electric Cooperative tests and our calculations, 15-kv fuses, cutouts and reclosers on a 13.8/23.9-kv system would be exposed to excessive recovery voltages when interruptiug phase-to-phase faults or when interrupting a line-to-neutral fault on one phase during a simultaneous line-to-neutral fault on another phase. Because of these considerations their use is not recommended. We would suggest that a co-ordinated line of fuses, cut-outs, reclosers, switches, and similar devices be considered specifically for 25-kv distribution class multigrounded neutral systems.

We solicit the authors opinions on the maximum future fault current level which will be encountered on these systems, the maximum recovery voltage and the long-term market for apparatus specifically designed for this service.

References

1. See reference 1 of the paper.
2. Report Number 37, Engineering Report of the Joint Subcommittee on Development and Research, Edison Electric Institute and Bell Telephone Systems (New York, N. Y.), volume IV, 1937, pages 303 and 313.
3. Construction Contract for Rural Electrical Distribution Project. Superintendent of Documents, Washington, D. C., REA form DS-7R.
4. REA Construction Manual. Westinghouse Electric Corporation (East Pittsburgh, Pa.), B-3323-A, pages 76–7.

Wade M. Edmunds and Glenn B. Roloson: Sumner has commented on the probability that the bushings on the transformers installed in 1916 were comparatively small with low-flashover characteristics which would provide relatively good lightning protection at the expense of an abnormal number of fuse outages. Some transformers were installed in 1916, and at various times since, and a large proportion of the total number of transformers were purchased about 1942 and are, therefore, modern transformers with standard size bushings. The statement that the operating experience with the transformers has been excellent does not indicate an abnormal number of outages.

We are pleased that Cunningham's calculations were more in accordance with the tests than the values obtained from the curves in the Westinghouse Electrical Transmission and Distribution Reference Book.

However, there does not seem to be any indication in the book that these curves are not applicable to rural distribution conductor sizes.

Electric Propulsion in Naval Surface Vessels

N. T. JUCARONE
MEMBER AIEE

SHORTLY preceding and during the recent war, the United States Navy constructed or acquired more than 1,100 surface vessels powered with electric propulsion. These vessels included such types as escort vessels, minesweepers, cargo and troop transports, tugs, tenders, tankers, net laying ships, salvage and rescue ships, oilers and oil barges, and ferryboats. The total installed capacity in these ships has exceeded 4,500,000 shaft horsepower.

Although the Navy has been equipping submarines with electric drives for many years, the only electric drives installed in naval surface vessels prior to about 1938 were limited essentially to one collier built in 1913, and to nine capital ships constructed in the decade following World War I. While these early surface installations incorporated the latest features of their period, as described in previous literature,[1,2] the electric propulsion installations in naval service vessels during the World War II period naturally incorporated many improvements as dictated by the most recent design and engineering practices. This paper will therefore be concerned primarily with the recent installations of electric propulsion in naval surface vessels.

It is the author's intention to consider the factors which influenced the use of electric propulsion in naval surface ships, to describe the several types of electric drives employed, and to review some of the major problems associated with these applications. Previous articles,[3,4,5] in recent publications have considered the advantages and disadvantages of electric propulsion and have reviewed applications for merchant and other commercial vessels.

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N. T. Jucarone is with the Bureau of Ships, Navy Department, Washington, D. C.

Factors Influencing the Use of Electric Propulsion in Naval Surface Vessels

The first electrically propelled naval surface ship, the collier *Jupiter*, which was later converted to the aircraft carrier *Langley*, was equipped with a twin screw a-c turbine electric propulsion plant which was rated about 5,500 shaft horsepower.

This was essentially an experimental installation. The favorable service record of this vessel strongly influenced the choice of a-c turbine-electric propulsion for the seven battleships and the two aircraft carriers constructed shortly after World War I. There were other important considerations, however. Earlier naval ships had been powered with slow-speed prime movers connected directly to the propeller shafts. The desire to employ high-speed steam turbines for propulsion in subsequent naval vessels, because of their low weight and space requirements as compared with slow-speed prime movers, introduced a need for a suitable speed reduction transmission between these high-speed prime movers and the slow-speed propellers. After considering the possibilities of reduction gears and electric propulsion, the latter was selected for these nine capital ships because it allowed greater flexibility in the arrangement of machinery, more economical operation at cruising speeds, and because the development of large reduction gears had not reached the stage where they could be considered reliable for service in naval capital ships.

Subsequent to the construction of these electrically propelled capital ships, however, the turbine geared drive came into extensive use in merchant and passenger vessels and proved itself beyond doubt. Accordingly, when the construction of capital ships was resumed after 1930, the Navy standardized on the turbine geared drive because of the considerable saving in weight and space obtainable by the use of reduction gears in lieu of electric propulsion equipment in plants of high power capacity. Consequently, no further electric drives have been installed in capital ships.

On the other hand, the use of electric propulsion in naval surface vessels requiring propulsion plants of small or medium capacity proved to be more favorable. Prior to World War II electric propulsion had been installed in many commercial vessels equipped with propulsion plants of small or medium capacity and had demonstrated its suitability in these applications. This increasing popularity in commercial applications and considerations of procurement, installation and operation led to the use of electric propulsion in many naval surface vessels requiring plants of corresponding capacity. Some specific considerations which have been of primary importance in the selection of electric propulsion for such naval applications may be noted.

1. Since no rigid mechanical tie is required between the prime movers and the propeller shaft with the electric drive, it permits maximum flexibility in the selection of the number, type, size, and speed of the prime movers, and considerable latitude in the arrangement of machinery. This is of special importance when the prime movers are Diesel engines. During the war, when the available engines were limited to certain types, sizes, and speed ratings, electric propulsion permitted the use of the most readily available types and sizes of engines, and shipboard arrangements best suited to the limited space available.

2. Since electric propulsion provides a means for obtaining speed reduction between prime movers and propeller shafts,

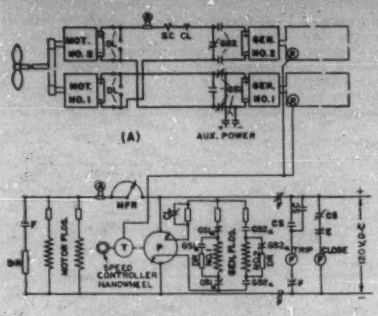


Figure 1. Simplified schematic diagram of d-c series loop Diesel-electric propulsion system

(A)—Series loop circuit
(B)—Excitation and control circuits

Contacts shown in positions corresponding to generator number 1 supplying propulsion power and generator number 2 de-energized. Excitation and control circuits for auxiliary power connections not shown

GS1 and GS2—Generator set-up switch contacts
GS1a and GS2a—Generator set-up switch auxiliary contacts
DL—Disconnect links
SC—Short circuit relay
CL—Current limiting relay
CS—Control switch
F—Main field contactor
E—Interlock contact on speed controller—closed in "off" position only
P—Generator field potentiometer
T—Engine speed control transmitter
R—Engine speed control receiver
MFR—Motor field rheostat
DR—Field discharge resistor

reduction gears may be eliminated. When the availability of reduction gears for propulsion service became critical during the war, electric drives utilizing slow-speed propulsion motors eliminated this bottleneck in the procurement of propulsion plants for many naval vessels, inasmuch as electric propulsion equipment could be procured more readily than large reduction gears. This was an important consideration in the destroyer escort program during the war. In these vessels, both d-c Diesel-electric and a-c turbine-electric propulsion were used, in addition to Diesel geared and turbine geared drives, because of the lack of sufficient gear production capacity to meet the urgent and extensive shipbuilding schedule.

3. The exceptionally favorable operating flexibility of the d-c electric drive at reduced speeds and under towing conditions makes it highly desirable for certain classes of naval surface vessels, such as tugs, ice breakers, salvage ships, and net laying ships.

4. The superior maneuverability of a-c and d-c electric drives as compared with geared or direct drives with fixed pitch propeller, is an important advantage for certain classes of naval vessels.

5. In a vessel equipped with electric propulsion, the propulsion generators may be employed for supplying power to operate large motor-driven auxiliary machinery and for other special purposes, thereby eliminating the need for additional ship's service generating capacity for such requirements. This feature has been used to advantage in minesweepers, tugs equipped with large fire pumps, tenders, tankers, and salvage vessels.

D-C Diesel-Electric Installations

Approximately 750 of the electrically propelled naval surface ships have d-c Diesel-electric drives. While the capacities of these drives range from a few hundred to 13,300 shaft horsepower, the majority are from 800 to 6,000 shaft horsepower. In general, vessels with twin screws have separate and independent plants for each propeller. Each plant contains one to four Diesel-generator sets, one to four propulsion motors, a propulsion control cubicle and the required auxiliaries. The motors are either slow-speed machines, with one double armature or two single armature units in tandem connected directly to the propeller shaft; or high-speed machines, with one to four motors connected to a reduction gear.

A simplified schematic diagram of a plant containing two Diesel-generator sets and two high-speed motors is shown in Figure 1. The generators and motors are connected in a closed ungrounded series loop with the motors and generators alternated so as to avoid the higher potential differences which would exist if the two generators were connected in succession. Each propulsion generator may be connected into or disconnected from the loop, or connected to supply power for special services where desired, by means of a set-up switch. Each set-up switch is provided with a by-pass contact in addition to the contacts for connecting or isolating the generator. The by-pass contact is normally closed when the generator is disconnected, thereby maintaining the continuity of the series loop. When the switch is operated, its opening and closing contacts overlap for a short interval so as to maintain the continuity of the loop while a generator is being connected into or disconnected from the loop. Since the generator armature is short-circuited during this transition, it is necessary to interlock the generator field contactor with the corresponding set-up switch in order to avoid the high circulating currents which would flow if the generator field were energized. With the foregoing arrangement, a generator may be added to or removed from propulsion service without de-energizing any other generators supplying propulsion power.

Disconnect links are provided for each propulsion motor so that it may be disconnected and by-passed in the event of a motor casualty.

Propeller speed is controlled by varying the generator field excitation and engine speed. The direction and magnitude of the generator field current are controlled by means of a potentiometer or similar device, while the engines are maintained at idling speed, which is usually 50 per cent of rated speed. When full generator field excitation is reached, further increase in propeller speed is obtained by increasing the speed of the engines gradually up to rated speed. Engine speed is controlled remotely by means of a mechanical, pneumatic, or electrical linkage which varies the setting of the speed governors on all engines in service simultaneously. The generator field potentiometer and the engine speed control transmitter are mechanically connected to a single handwheel mounted on the front of the propulsion control cubicle, thus permitting control of propeller speed and reversing to be accomplished simply by manipulating the handwheel. Where remote control from the pilot house is desired, a similar speed controller may be provided in a pilot house control stand.

Figure 2 illustrates the manner in which the generator armature voltage, field current, and engine speed vary as the speed controller handwheel is moved to ahead and astern positions. Since the motor armature voltage varies directly as the generator armature voltage, the propeller speed will vary practically in proportion to armature voltage, if the motor field current is maintained constant.

The combination of variable voltage and engine speed control makes it possible to vary the propeller speed smoothly over the entire range from zero to rated speed, either ahead or astern, and to operate continuously even at "creeping" propeller speeds. Also, since the torque developed by d-c motors is determined practically by the product of field flux and armature current, and since the armature current may increase to values of 150 to 200 per cent of rated current while the armature

voltage is at reduced values during starting and reversing, it is possible to obtain peak motor torques equal to 150 to 200 per cent of rated value without overloading the Diesel engines. These desirable features, which are obtainable because of the variable speed transmission ratio and the favorable torque characteristics of d-c motors, make the d-c electric drive superior to the a-c electric drive and to the geared or direct drive with fixed pitch propeller, from the standpoint of maneuverability.

Additional flexibility of control is obtained by the use of a motor field rheostat, which permits varying the speed of the propulsion motors over a predetermined range independently of the voltage and speed adjustments of the Diesel-generator sets. Thus, the motor field rheostat provides a means for varying the speed transmission ratio without varying the generator field excitation. This rheostat is employed primarily for adjusting propeller speed so as to obtain rated output from the Diesel-generator sets in service when any of them are secured or when the propeller load changes because of hull fouling, displacement changes, or other external conditions which vary the ship's resistance to motion. These functions of the rheostat may be explained by reference to Figure 3. Under steady operating conditions the power versus speed characteristic for a fixed pitch propeller is closely approximated by a cubic relation, such as curve 1. When both Diesel-generator sets in a two engine plant are operating at rated output and speed and when the propeller output and speed are also at rated values, operating conditions correspond to point A on curve 1. If one Diesel-generator set is removed from service and no other changes are made, the motor armature voltage is reduced to 50 per cent of its rated value and hence the propeller speed drops to 50 per cent of rated speed. Under this condition the propeller output, as determined by point B on curve 1, is only 12.5 per cent of rated power, and the output of the Diesel-generator set in service is reduced to only 25 per cent of its rated capacity, although the set is still operating at rated speed. By means of the motor field rheostat, however, the propeller speed may be increased until the propeller output is 50 per cent of rated shaft output, as indicated by point C on curve 1. At this new propeller speed, which is 79.4 per cent of rated value, the Diesel-generator set in service will be delivering its rated capacity at rated speed. Hence, in the two engine plant, a range of at least 29.4 per cent speed increase by motor field control is required to obtain rated engine output when one set is secured.

Likewise, when external conditions increase the ship's resistance, more power is required to turn the propeller at a given speed. Hence curve 1 assumes a new position, such as shown by curve 1′. The position of this second curve is not intended to be quantitative, since this depends upon how much the ship's resistance increases. Under the new operating conditions, the propeller output at rated speed would exceed rated value, as indicated by point D on curve 1′, and the two Diesel-generator sets would be seriously overloaded. By means of the motor field rheostat, however, the propeller speed may be reduced until the propeller output is restored to rated value, as indicated on point E on curve 1′, and the Diesel-generator sets are delivering rated capacity at rated speed. The load on fixed pitch propellers designed for towing service usually is maximum when the vessel is stationary and becomes equal to rated power at about 75 per cent of rated propeller speed. Hence, in such applications, a range as high as 25 per cent speed reduction by motor field control is very desirable in order to utilize the full output of the plant under maximum tow conditions.

For the two engine plant considered in the foregoing example, a total range of speed variation by motor field control equal to at least 55 per cent of rated speed would be required in order to obtain the desired flexibility of control in vessels in which towing is a primary function.

The electric protective devices shown in Figure 1 consist of a short-circuit relay and a current-limiting relay, both of which have their operating coils connected in the propulsion-loop circuit. The short-circuit relay operates at 250 to 300 per cent of rated loop current and when actuated, trips the motor and generator main field contactor, thereby de-energizing the propulsion loop in the event of excessive current. This relay resets automatically and the operator may restore excitation simply by returning the speed controller to the "off" position in the event that tripping of the relay was caused by peak currents of short duration. The current limiting relay operates at lower values of loop current, usually 110 to 125 per cent of rated value; its function being to limit the peak currents and the rate of current rise during maneuvering, turning, and rapid reversals. When actuated, this relay inserts additional resistance in the excitation circuit of the propulsion generators. The resistance is retained in the excitation circuit until the loop current returns to rated value.

Propulsion excitation and control power is obtained from any reliable 120- or 240-volt d-c source, which may be the ship's service d-c system, motor-generator sets, or exciters directly connected to or belt-driven by the propulsion Diesel-generator sets. Separate exciters may be provided for the propulsion motors and generators, with control effected by regulating the field current of these exciters.

A typical propulsion control cubicle used in harbor tugs is shown in Figure 4. In this instance a single multiposition set-up switch, actuated by the left handwheel, was provided for the two propulsion generators. The handwheel-operated main speed controller is shown in the

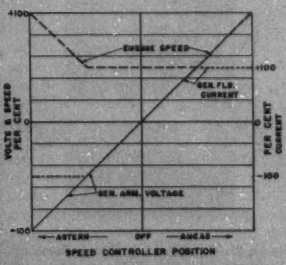


Figure 2. Variation of engine speed, generator field current and armature voltage with speed controller positions

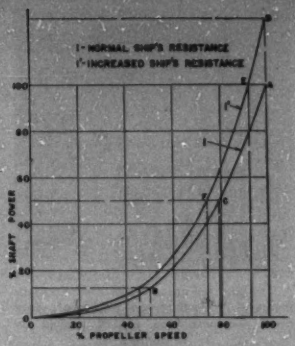


Figure 3. Relation of shaft power to propeller speed

Points A and E—Maximum shaft power and speed with all engines in service in a d-c Diesel-electric plant

Points C and F—Maximum shaft power and speed with 50 per cent of engines in service in a d-c Diesel-electric plant

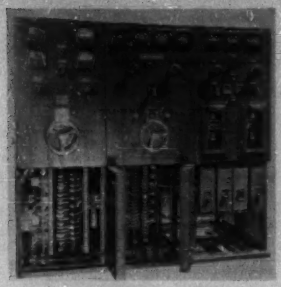


Figure 4. Westinghouse drip-proof, dead front, d-c propulsion control cubicle for harbor tugs

Lower panels removed or opened
From left to right: Generator section, motor section and exciter section

center, with motor field rheostats located directly above. The two voltage regulators at the right are required for maintaining the voltage of the two direct-connected propulsion exciters constant throughout the engine operating speed range. The four circuit breakers below the regulators are for connecting either exciter to supply propulsion excitation and control power or auxiliary ship's service power. The propulsion cables enter from below.

D-C Propulsion Motors and Generators

These are heavy-duty marine-type class-B insulated, separately-excited shunt machines. Propulsion generator capacities range from 150 to 1,375 kw, at rated speeds of 600 to 900 rpm. Propulsion motor capacities range from 200 to 5,000 hp, at rated speeds of 100 to 600 rpm for direct-connected motors and 700 to 1,500 rpm for geared motors. Armature voltages are maintained under 1,000 volts so as to minimize personnel hazards and insulation problems. Some features of interest in naval applications will be considered in the following paragraphs.

In applications where towing duty is important, the propulsion motors are designed for rated capacity over a speed range of about 25 per cent in order to match the propeller load requirements under maximum towing conditions.

When engine capacities are below about 1,000 brake horsepower, each propulsion generator usually is provided with a series field for starting the Diesel-generator set electrically from a storage battery.

The machines are constructed as light in weight and as compactly as practicable without sacrificing reliability and ruggedness. In order to minimize damage from shock, Navy specifications require steel construction, body-bound coupling and foundation bolts, and locking devices to prevent brushes from becoming dislodged. Cast iron and other materials of low impact strength are not acceptable.

Pressure-lubricated or self-lubricated sleeve-type bearings are preferred because of the greater reliability and easier maintenance of such bearings in machines of large capacity, as compared with ball or roller bearings. The same type of lubricating oil is used for motor and generator pressure-lubricated bearings as is required for the Diesel engines.

Careful attention is given to the arrangement of terminal connections and to the wiring around the frame in order to minimize the external magnetic fields which would be produced by unbalanced ampere turns around the motor or generator shaft, since such magnetic fields cannot be completely neutralized with degaussing coils and may be of sufficient magnitude to create a hazard in mined waters. External fields have been eliminated so effectively that even minesweepers have been equipped with d-c propulsion machines with complete confidence.

Experience has shown that electric strip heaters installed in each propulsion motor and generator are very desirable as a means of maintaining insulation dry when the machines are secured, particularly in tropical climates.

The problem of providing adequate ventilation for the propulsion motors and generators has required considerable attention. In the majority of machines of 1,000 hp or 1,000 kw capacity, ventilating air is drawn from the machinery spaces and is circulated through the machines by fans attached to the rotors or separately motor-driven. When the heated air from these machines is exhausted into the machinery spaces, an excessive rise in ambient temperature results and a considerable amount of the heated exhaust air is recirculated into the machines, unless an elaborate machinery space ventilating system is provided and barriers to prevent recirculation are installed. These undesirable conditions were aggravated by the necessity of operating in tropical climates during wartime with hatches and openings to the weather secured. In order to overcome these undesirable conditions, the use of ductwork attached directly to the exhaust outlets of the motors and generators and discharging the exhaust air outboard, was resorted to in many vessels. However, while this arrangement overcame the difficulties noted, it introduced a new problem, since the machines became exposed to damage from water entering or condensing inside the ductwork and subsequently entering the machines. As a consequence, the Navy eventually resorted to the use of enclosed machines with water-to-air heat exchangers, for propulsion motors and generators of all capacities. These self-contained water-cooled machines have proved very satisfactory in overcoming the problems of ventilation. In addition, the completely-enclosed construction makes it easier to protect the machines from water damage and from the oil and dirt always present in the machinery spaces.

Figure 5 is a sectional view of a large d-c propulsion generator, showing the double-tube type cooler, sleeve type pressure-lubricated bearings, and the fan attached to the rotor. The frame and armature spider are constructed of fabricated steel to reduce weight. The casing below the frame feet is watertight to protect against flooding when the water level in the bilges rises.

A-C Diesel-Electric Installations

Only two naval vessels have been equipped with a-c Diesel electric propulsion. The total capacity installed in the first vessel, a twin screw catapult lighter, was 1,100 shaft horsepower. Since this vessel was never actually placed in service it will not be considered further in this paper. The second naval vessel with this type of drive is the submarine tender *USS Sperry*. This twin screw, 10,000 ton

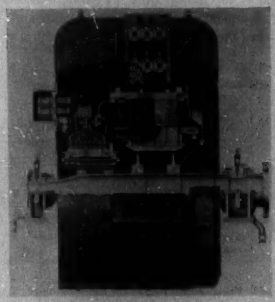


Figure 5. Allis-Chalmers Diesel-driven, enclosed-type d-c propulsion generator, with water-to-air cooler

Rating: 1,200 kw, 525 volts, 750 rpm

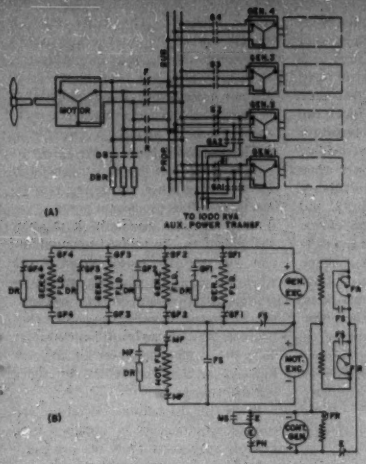


Figure 6. Simplified schematic diagram of a-c Diesel-electric propulsion system installed in *USS Sperry*

(A)—Propulsion power circuits

(B)—Excitation and control circuits

Contacts shown in positions corresponding to generators numbers 1 and 2 supplying propulsion power for forward operation, and generators numbers 3 and 4 de-energized. Circuit breakers and field contactors are magnetically operated by operating levers. Excitation and control circuits for auxiliary power connections not shown

G1, G2, G3, G4—Generator propulsion circuit breakers

GA1 and GA2—Generator auxiliary power circuit breakers

F and R—Motor forward and reverse circuit breakers

DB—Dynamic braking circuit breaker

DBR—Dynamic braking resistors

GF1, GF2, GF3, GF4—Generator field contactors

MF—Motor field contactor

FS—Field starting and running contactor

E—Exciter contactor

MS—Master switch contact—closed with motor operating lever in "stop" position only

PH—Phase balance relay contact

FR—Field rheostat

DR—Field discharge resistor

ship which was completed in 1942, is of considerable interest since, it is the first naval ship and the largest commercial or naval vessel equipped with a-c Diesel-electric propulsion ever placed in operation in the United States. Consequently this tender has served as a trial ship for demonstrating and evaluating the a-c Diesel-electric drive. The highly successful trials and creditable performance of the *Sperry* have substantiated the Navy's interest in this type of drive. Further installations were being considered for additional submarine tenders, but termination of hostilities interrupted the construction program.

The *Sperry* has two separate and independent propulsion plants, one for each propeller. Each plant is rated 5,900 shaft horsepower, and contains four Diesel-generator sets and one slow-speed synchronous propulsion motor. A simplified schematic diagram of the plant for one shaft is shown in Figure 6. The synchronous generators may be connected in parallel in any combination from one to four, depending upon power requirements. Since the generators have salient poles with damper windings in the pole faces, any unit can be connected to the bus simply by bringing it up to approximately synchronous speed, closing its circuit breaker and energizing its field. Synchronizing instruments are not required. In fact, it was found possible during the trials of this vessel to start a Diesel-generator set from rest by motorizing its generator from the propulsion bus. Any propulsion generator may be readily added to or removed from the bus without removing power from the propeller.

Since each propulsion motor also has salient poles and a damper winding, it can be started as an induction motor and synchronized when up to approximately synchronous speed. Reversing is accomplished by reversing two of the phase leads to the motor. Prior to reversing, the motor is slowed down rapidly by dynamic braking. After the motor has been reversed and accelerated in the reverse direction up to approximately synchronous speed, it is again synchronized. During the trials of the *Sperry* the period of dynamic braking and the amount of braking resistance were varied until optimum conditions for most rapid propeller reversals without stalling the engines were realized. After these adjustments had been made, successful reversals were accomplished with only two engines in service per shaft.

Propeller speed is controlled by varying the speed of the Diesel-generator sets in service simultaneously over a speed range from 30 per cent to rated engine speed, thereby varying the frequency and the motor speed proportionally. During starting and reversing, the Diesel-generator sets are maintained at the idling speed of 30 per cent rated speed until the motor is synchronized, and strong over-excitation is applied to the propulsion generators. Strong over-excitation is applied also to the propulsion motor during the synchronizing period so as to obtain maximum pull-in torque. Control of engine speed, field excitation, and of the motor and generator circuit breakers is accomplished by means of operating levers located at the front of the propulsion-control cubicle. A more detailed description of the a-c Diesel-electric propulsion control system has been ably presented in an earlier paper.[6]

Because of the fixed speed transmission ratio between the engines and the synchronous motor in each plant, and in view of the less favorable torque characteristics of a-c machines as compared with d-c machines, the operating characteristics of the a-c Diesel-electric drive are not quite as favorable as for the d-c Diesel-electric drive. With the a-c drive, the propeller cannot be operated continuously at speeds below 30 per cent of rated speed without the addition of special equip-

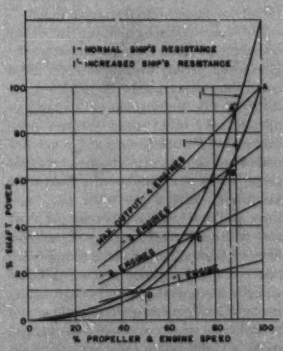


Figure 7. Relation of shaft power to propeller and engine speed in a four engine a-c Diesel electric plant

Points A, B, C and D—Maximum shaft power and speed, normal ship's resistance, with 4, 3, 2 and 1 engines in service, respectively

Point A'—Maximum shaft power and speed, increased ship's resistance, with four engines in service

Figure 8. Westinghouse drip-proof, dead-front, a-c propulsion control cubicle for USS *Sperry*

ment, and the maximum motor torque which can be developed during starting and reversing is limited to not more than about 110 per cent of rated torque. On the *Sperry* it was found necessary to install a torque regulator in each plant in order to limit the maximum output of the engines during induction motor operation, when starting and reversing. This regulator functions to insert resistance in the field excitation circuit of the propulsion generators when the engine speed is reduced below a predetermined value because of insufficient engine torque.

The a-c Diesel-electric drive is limited also with respect to the maximum power obtainable when one or more of the engines are secured and when the propeller load changes because of a change in external conditions. Referring to Figure 7, when four Diesel-generator sets are operating at rated capacity and rated engine speed, steady operation under rated conditions corresponds to point A on curve 1. If the engines are capable of operating at rated mean effective pressure over their operating speed range, the maximum output of each engine will be proportional to engine speed. Hence, the maximum shaft power with one, two, three and four engines in service per shaft may be represented by the four straight lines in Figure 7 which intersect the 100 per cent speed ordinate at 25, 50, 75 and 100 per cent shaft power, respectively. If one Diesel-generator set should be removed from service without changing the speed of the three sets remaining in service, these sets would be seriously overloaded, since the three engines would be required to deliver rated shaft power, or 133 per cent of rated engine capacity per engine, as long as the propeller operated at rated speed. To avoid overloading the engines in service their speed must be reduced until operation corresponds to point B on curve 1, which is the intersection of curve 1 and the curve for maximum output with three engines in service. At point B the shaft power is 65 per cent of rated value and the output from each engine in service is 86.7 per cent of rated engine capacity. This is the maximum shaft power and engine output which can be obtained when three Diesel-generator sets are in service without overloading them. Similarly, the shaft power when only two and one Diesel-generator sets are in service is determined by points C and D, respectively. The maximum engine outputs corresponding to points C and D are 70.7 and 50.0 per cent of rated engine capacity, respectively. Thus, in contrast to the d-c electric drive, in which rated output can be obtained from the prime movers irrespective of the number of units in service, with the a-c Diesel-electric drive it is necessary to operate at less than rated engine capacity when any of the sets are secured.

Likewise, when external conditions cause the propeller load to increase, as represented by curve 1′ in Figure 7, it is also necessary to reduce the speed of the Diesel-generator sets in service, and consequently the propeller speed, in order not to overload the engines. The maximum engine output and shaft power under the new conditions when four diesel generator sets are in service per shaft are determined by point A′ on curve 1′. As in the previous case, the maximum engine output is less in the a-c Diesel-electric drive than in the d-c electric drive, because of the necessity of reducing the speed of the Diesel-generator sets in the former drive when external conditions cause the propeller load to increase.

It should be noted that the foregoing limitations apply not only to the a-c Diesel-electric drive, but also to the Diesel geared and direct drives with fixed pitch propellers, since the speed transmission ratio is fixed in these drives also. However, in the majority of naval surface vessels with propelling plants of medium or higher capacity and in which the a-c Diesel-electric drive appears to be more promising because of its higher over-all efficiency at rated output, greater reliability and lower weight as compared with the d-c Diesel-electric drive, the above limitations are usually not too serious. In such vessels, a high degree of maneuverability and superior towing characteristics are in general not primary considerations.

The electric protective devices consist of a phase-balance relay and a ground relay. The former provides protection in the event of a severe unbalance in phase voltages, by de-energizing the field circuits of the propulsion motor and generator exciters. The latter relay is energized when a ground occurs, causing an alarm to sound and an indicating light to flicker. Since the propulsion circuits are not grounded, it is not essential to de-energize the system immediately in the event of a single ground.

The propulsion plants in the *Sperry* are designed to utilize propulsion generators for supplying other ship power requirements as well as propulsion power. Any of the propulsion generators in the port shaft plant can be utilized to supply an 850 kw, a-c/d-c motor-generator set which is used for charging submarine storage batteries; and two of the propulsion generators in either the port or starboard shaft plant can be utilized to supply 450-volt a-c 60-cycle power for large arc furnaces and for other services, through a suitable transformer.

Power for propulsion excitation and control requirements is obtained from three motor-generator sets, one being provided for each plant and the third being a standby unit for either plant. Each set consists of an a-c motor, a propulsion motor exciter, a propulsion generator exciter and a control power generator. Excitation and control circuits are 120 volts, direct current. During starting and reversing, generator field over-excitation is obtained by connecting the motor and generator exciters in series to supply 240 volts.

Figure 8 shows one of the two propulsion control cubicles installed in the *Sperry*. The high-voltage circuit breakers and instrument transformers are located

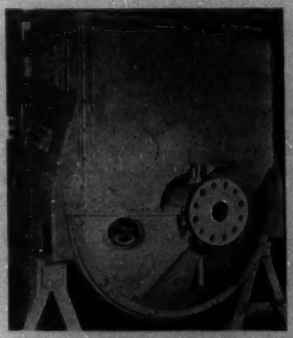


Figure 9. Westinghouse Diesel-driven, enclosed type, a-c synchronous, salient pole propulsion generator with water-to-air cooler for USS *Sperry*

Rating: 1,150 kva 2,400 volts 3-phase unity-power factor 62.5-cycles per second 750 rpm

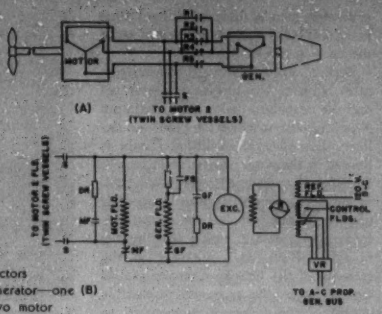


Figure 10. Simplified schematic diagram of a-c turbine-electric propulsion system

(A)—Propulsion power circuit
(B)—Excitation and control circuits

Contacts shown in positions corresponding to forward operation. Line and field contactors are mechanically operated by operating levers

R1 to R5—ahead-astern line contactors
S—Set-up switch for one generator—one motor or one generator—two motor operation in twin screw vessels
GF—Generator field contactor
MF—Motor field contactor
FS—Field starting contactor
R—Rotary amplifier
VR—Constant volts per cycle regulator
DR—Field discharge resistor

in the rear section and the low-voltage equipment is located in the front section; the two sections being separated by a passageway at the center and entered through the door. The dynamic braking resistors are shown at the top center of the cubicle. The operating desk contains four levers for controlling each of the four propulsion generator line circuit breakers and field contactors; one lever for controlling the propulsion motor starting, reversing and dynamic braking circuit breakers and the motor field contactors; one lever for controlling the 850-kw motor-generator set line circuit breaker; and one lever for regulating the engine speed master transmitter and motor and generator field excitation. The handwheel to the right controls the exciter selector switch.

A-C Propulsion Motors and Generators

These are heavy-duty marine-type class-B insulated 3-phase salient-pole synchronous machines, with damper windings in the pole faces. Each of the eight propulsion generators is rated 1,150 kva, unity power factor, 2,400 volts, alternating current, 750 rpm. Each of the two propulsion motors is rated at 5,900 horsepower, 2,400 volts, alternating current, 139 rpm. Operating frequencies range from 18.7 cycles per second at idling speed to 62.5 cycles per second at rated engine speed. The machines are completely enclosed and are equipped with water-to-air heat exchangers of double tube construction. The generators have ventilating fans attached to the rotors and the motors are provided with motor-driven blowers. The same considerations for obtaining light weight, compactness, resistance to shock and reliability govern in the construction of these a-c machines as were discussed in the case of d-c propulsion machines.

Figure 9 is a view of the coupling end of one of the eight propulsion generators installed in the *Sperry*. Two fans, one attached to each end of the rotor, circulate the ventilating air radially through the stator and through the cooler located on top of the frame. The terminals, shown exposed, are taped and enclosed in a drip-proof terminal box when installed. The pressure-lubricated sleeve bearings are supported directly on the end bells. This is preferred to the usual pedestal-type bearing because of the reduction in over-all length and the reduced possibility of the rotor shifting with respect to the stator.

A-C Turbine-Electric Installations

Approximately 350 of the electrically propelled naval surface vessels have been equipped with a-c turbine-electric drives. While the capacities of these range from 2,500 to 20,000 shaft horsepower in the recent applications, the majority are from 3,300 to 12,000 shaft horsepower. All of the installations made during the World War II period have a separate plant for each propeller; each plant consisting of a single turbine-generator set and a slow-speed propulsion motor. Single units are used in preference to multiple units because of the reduced weight per horsepower and higher efficiencies obtainable with turbines, generators, and motors of increased capacity; and in view of the high degree of reliability of steam turbines and a-c machines. While each plant in a twin screw vessel is entirely self-contained and may be controlled independently of the other plant, either propulsion generator may be connected to supply the two propulsion motors simultaneously. This added flexibility permits operating at reduced speeds with only one turbine-generator set in use, thus improving the operating efficiency at reduced speed and providing an opportunity for inspecting and servicing the secured set while underway.

A simplified schematic diagram of a plant having one turbine-generator set and one motor is shown in Figure 10. The electric power and control system is similar to that described for the a-c Diesel-electric drive except for the simplifications introduced by the use of only one generator and motor per plant, and differences to be noted in the following. In most turbine-electric drives dynamic braking is not required since the high inherent torque capacity of steam turbines at reduced speeds practically eliminates the possibility of stalling them during rapid reversals. Dynamic braking was found necessary in only one class of vessels, which consisted of seven troop transports, each having two propellers and two 10,000 shaft horsepower propulsion plants. In this instance, rapid reversals conducted before dynamic braking was provided causing the damper windings in the propulsion motors to overheat and their brazed connections to loosen. The installation of dynamic braking resistors remedied this difficulty. This application proved to be a good example of the importance of carefully correlating the ship and propeller characteristics with the capacity and design characteristics of the a-c propulsion system.

The line contactors are interlocked with the field contactors so that the line contactors cannot be operated while the field excitation circuits are energized. Consequently, the line contactors need be designed to interrupt only a fraction of rated power.

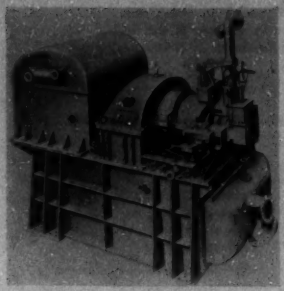
As in the a-c Diesel-electric drive, propeller speed is controlled by varying the speed of the turbine-generator set. However, a greater range of speed variation is obtained since the turbine speed is varied from rated to as low as 15 per cent of rated speed.

When steam turbines are employed as prime movers, stability requirements influence the design of the a-c propulsion system to a greater degree than when the prime movers are Diesel engines. In the a-c Diesel-electric drive, momentary increments in propeller torque, such as are caused by rolling, pitching and turning, are reflected in momentary reduction in the speed of the Diesel engines because of

the relatively low inertia of the rotating masses and the lag in response of the speed control system. The reduction in propeller speed which results is effective in limiting the magnitude of such torque increments. In addition, Diesel engines are usually provided with mean effective pressure limiters which reduce the engine speed, irrespective of the governor setting, when engine torque exceeds rated torque by 10 to 15 per cent. Hence, in a-c Diesel-electric drives, propulsion motors and generators designed with a torque margin of only 10 to 15 per cent more than rated torque at normal excitation, will usually be adequate to insure stability when the propeller torque varies momentarily. When the prime movers are steam turbines, however, the inertia of the rotating masses is relatively high, and consequently momentary increments in propeller torque are transmitted through the propulsion motor and generator to the turbine without any perceptible reduction in the speed of the system. Hence, in a-c turbine-electric drives, the a-c propulsion system must be designed to absorb higher momentary torque increments than in a-c Diesel-electric drives. In order to avoid the use of oversize propulsion motors and generators, the usual practice is to provide a fast acting regulator, such as an amplidyne or a Rototrol, in the control system of a-c turbine-electric drives. The regulator is usually connected to regulate motor and generator field excitation automatically so as to maintain the ratio of generator voltage to frequency essentially constant and the system power factor at unity throughout the operating speed range. Momentary increments in propeller torque cause the regulator to increase the amount of motor and generator field excitation correspondingly, and hence the torque margin of the machines is raised momentarily a sufficient amount to insure stability. By the use of such regulators, in conjunction with turbine steam flow regulators which limit the maximum turbine overload, motors and generators may be designed with a torque margin as low as only 5 per cent of rated torque.

Because of the fixed speed transmission ratio between the turbine-generator set and the propulsion motor, the maximum sustained output which the plant can deliver when external conditions change is limited in the same manner as in the a-c Diesel-electric drive. However, the limitation in maximum sustained output is usually determined by the propulsion motor and generator rather than by the capacity of the steam turbine. If the capacity of the turbine-generator set, as determined by the maximum permissible output of the generator, is assumed to vary linearly with speed throughout the operating speed range, then the curve for four engine maximum output in Figure 7 may be taken also as the maximum output curve of the turbine-generator set. Hence, if external conditions cause the propeller characteristic to be displaced from curve 1 to curve 1′ in Figure 7, then the speed of the turbine-generator set and of the propeller must be reduced until operating conditions correspond to point A′ on curve 1′, as in the case of the a-c Diesel-electric drive. Since only one turbine-generator set is provided in each plant, the limitations relative to the use of multiple prime movers per shaft considered previously for Diesel-electric drives do not apply. However, when one turbine generator set is used to supply both propulsion motors in a twin screw vessel, the maximum operating speed and output of the turbine-generator set in service must be limited in the same manner as when only two engines in a four engine a-c Diesel-electric plant are in service. Thus, when one turbine generator set is supplying power to both propulsion motors, maximum operating conditions will correspond to point C on curve 1 in Figure 7, when the ship's resistance is normal. At this point, the output from the turbine-generator set in service is 70.7 per cent of rated capacity, and each propeller is operating at 70.7 per cent of rated speed and absorbing 35.4 per cent of rated shaft power.

When steam turbines are used as prime movers, the advantages of using a-c machines in preference to d-c machines are so favorable that only a-c machines have been employed in turbine powered naval surface ships with electric drives. This is attributable primarily to the considerable savings in weight and space possible when high-voltage high-speed a-c generators are used in lieu of inherently low-voltage medium-speed d-c generators.



Assembly of turbine, generator and condenser

Generator stator winding

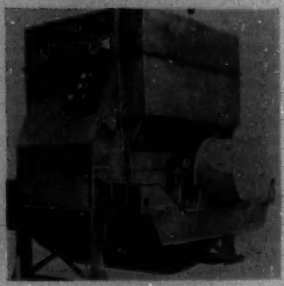
Generator rotor

Figure 12. General Electric propulsion turbine-generator set

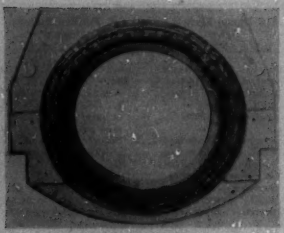
Rating: 4,600-kva 2,700-volts a-c, 3-phase, unity of power factor, 93.3 cycles per second, 5,600 rpm

Figure 11. General Electric drip-proof, dead front a-c propulsion control cubicle for turbine-electric plant

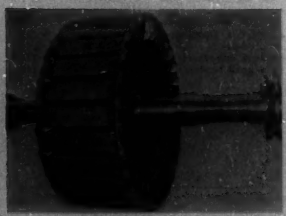
Coupling end view



Collector ring end view



Motor stator winding



Motor rotor

Figure 13. General Electric enclosed-type a-c synchronous salient pole propulsion motor with water-to-air cooler

Rating: 6,000 hp 2,700-volt 3-phase unity power factor, 93.3 cycles per second, 400 rpm

Figure 11 shows a propulsion control cubicle installed on the upper level in the machinery spaces of a turbine-electric surface vessel. The operating desk contains one motor set-up lever, one motor reverser lever, one field lever, one turbine governor speed control lever and one emergency turbine throttle control lever. The handwheel to the right is for turbine load limit control. Meters are Navy approved high-impact instruments.

A-C Propulsion Motors and Generators

These are similar in construction to the a-c machines described for the a-c Diesel-electric drive with the principal exception that the propulsion generators are cylindrical-rotor high-speed machines. The capacities of propulsion generators range from 2,000 to 7,650 kw, at rated speeds of 3,000 to 5,600 rpm. Capacities of propulsion motors range from 2,500 to 10,000 hp at rated speeds of 90 to 400 rpm. Rated a-c voltages range from 2,000 to 3,600 volts. Operating frequencies range from 50 to 93 cycles per second at rated speed.

In numerous installations, heat exchangers of single tube construction have been provided for the a-c propulsion motors and generators. Several serious casualties have occurred when leaks developed in the tubes of the coolers and resulted in salt water damaging stator and rotor insulation. The Navy's experience has fully justified its preference for coolers of double tube construction as a protection against such hazards.

Synchronous motors with damper windings in the pole faces are used exclusively in preference to induction motors in all recent naval applications because of the relatively lighter weight, higher efficiency and higher power factor obtainable with synchronous motors.

Figure 12 shows an assembly view of a complete propulsion turbine-generator set, and views of the generator stator winding and cylindrical rotor. The compactness and light weight but rugged construction are apparent. The rotor is supported on a single bearing at the free end and is connected by a forged-on flange directly to the turbine shaft.

Figure 13 shows several views of an a-c propulsion motor. The collector rings are located outside the motor enclosure for accessibility to brushes and collectors. Self-lubricated disk-type sleeve bearings are employed. The sturdy damper winding construction is apparent. In this application, the motor speed was sufficiently high to permit the use of fan blades attached to the rotor, in lieu of separate motor-driven fans as is more frequently the case.

Comparison of A-C and D-C Diesel-Electric Drives in Submarine Tenders

It is of interest to compare the a-c Diesel-electric installation in the submarine tender *Sperry* with the d-c Diesel-electric installations of comparable capacity in the six submarine tenders designated as *AS11*, *AS15* to *AS19*, inclusive. Each of these tenders has two propellers and propelling plants rated approximately 6,000 shaft horsepower per shaft. The prime movers in each vessel consist of eight General Motors model *16-278A* Diesel engines (four per shaft). Hence, these tenders provide an opportunity for comparing a-c and d-c electric propulsion systems in plants which are essentially similar with respect to application, capacity, prime movers, and installation features.

A summary of the weights of the major electric components in these plants is given in Table I. Although these weights

Table I. Weights of Electric Propulsion Equipment in Diesel-Electric Submarine Tenders

Electric Components	AS12—A-C Drives 11,800 Total Shaft Horsepower		AS13–19—D-C Drives 11,200 Total Shaft Horsepower	
	Rating	Number × Weight, Pounds	Rating	Number × Weight, Pounds
Propulsion motors	5,900 hp, 139 rpm	2×61,400	1,440 hp, 750 rpm	8×16,900
Reduction gears			5.64:1 ratio	2×55,500
Propulsion generators	1,150 kw, 750 rpm	8×10,300	1,125 kw, 750 rpm	8×16,900
Propulsion control cubicles		2×23,000		2×6,100
Propulsion M-G sets and controllers	180 hp/114.5 kw	3×11,500	60 hp/40 kw	4×3,800
Total weight, pounds		285,700		408,800
Pounds per shaft horsepower		24.2		36.5

Table II. Efficiencies of Electric Propulsion Equipment in Diesel-Electric Submarine Tenders

Vessels	Drive	Component Efficiencies: Propulsion Generators	Propulsion Motors	Reduction Gear	Over-all Transmission Efficiency
AS 12	A-C	97.8	97.0		94.9
AS15–19	D-C	95.2	95.2	98.0	88.7

Note: Efficiencies are based on rated output and are calculated from factory test data. Motor and generator excitation losses and power requirements for associated auxiliaries are not included.

are approximate, being based on manufacturers' design calculations, and do not include the weights of cable and auxiliaries, they are sufficiently accurate to indicate the considerable savings in weight realized with the a-c drive. Thus, the weight of the d-c electric equipment is approximately 1.5 times that of the a-c electric equipment. It should be noted that in the d-c drives, since high-speed d-c propulsion motors coupled to reduction gears are employed, the weight of the gears have been included also. If slow-speed motors had been employed, the weight of the d-c electric equipment would have been appreciably higher. The relatively higher weight of the a-c propulsion control cubicles, as compared with the d-c cubicles, is attributable primarily to the use of more elaborate control equipment and magnetically operated circuit breakers in lieu of manually operated circuit breakers or contactors. With the experience gained from this first installation, it is believed that in future a-c Diesel-electric drives, appreciable reduction in the weight of the control equipment, and hence reduction in the weight of the a-c electric equipment per shaft horsepower, should be possible.

The size of the propulsion rotating machinery is frequently of paramount importance because of the limited space available on the lower level in the machinery spaces. The space required for the a-c propulsion motors and generators is appreciably less than that required for the d-c motors, generators and reduction gears, in these tenders. On the other hand, the a-c control cubicles are almost twice as large as the d-c cubicles. However, as noted previously, future simplifications and refinements should result in relatively smaller a-c cubicles.

A summary of the component and over-all transmission efficiencies from the prime movers to the propeller shafts is shown in Table II. The higher over-all efficiency of the a-c drive is reflected in a slightly higher shaft output at rated capacity as compared with the d-c drive, even though the total prime mover capacity is the same for both drives. It should be noted, however, that at speeds and outputs appreciably below rated value the fuel consumption per shaft horsepower is probably somewhat lower for the d-c drives than for the a-c drive, since in the former the Diesel-generator sets in service can be operated at speeds corresponding to optimum output because of the variable speed transmission ratio obtainable with the d-c drives.

As has been expected, the a-c electric propulsion equipment in the *Sperry* has required less servicing than the corresponding d-c equipment in the remaining tenders. Some unanticipated difficulties have been experienced with both types of drives. In the a-c drive, on several occasions the interconnections between the field coils of the a-c propulsion motors loosened. This was remedied by resoldering the connections more carefully with silver solder. Breakage of fan vanes in the a-c propulsion generators also occurred and was corrected by bracing the vanes. In the d-c drives, the rotor fans in the propulsion motors and generators developed cracks in the fan blades. All of these fans are being replaced with fans of more rugged construction. The d-c machines have required more frequent cleaning and servicing than the a-c machines because of the accumulation of carbon and copper particles, in conjunction with oil and moisture, on the uninsulated surfaces in the d-c machines. In several instances, partial flooding of the d-c motors and generators has necessitated extensive overhauls and replacement of armatures and field coils. On the other hand, no major overhauls of the a-c machines in the *Sperry* have been required to date.

The foregoing comparison furnishes convincing support for the use of a-c electric propulsion equipment in Diesel-electric drives where plants of medium or higher capacity are required, and where the attainment of maximum flexibility and maneuverability is not of primary importance. A-c Diesel-electric propulsion should accordingly find many applications in plants having capacities of about 3,000 shaft horsepower per shaft and higher.

Status of Electric Propulsion in Naval Surface Vessel Applications

Based upon experience acquired with naval surface vessels constructed and operated during the recent war, considerable progress has been made in establishing the basic requirements essential in the

Table III. Summary of the Fields of Application of the Three Conventional Electric Drives in Naval Surface Vessels

	D-C Diesel Electric Drive	A-C Diesel Electric Drive	A-C Turbine Electric Drive
Most probable capacities	Up to about 3,000 shaft horsepower per shaft*	3,000 to 12,000 shaft horsepower per shaft**	Above about 3,000 shaft horsepower per shaft
Primary reasons for use	1. Optimum maneuverability	1. Adaptability with multiple Diesel engines per shaft	1. Availability of propulsion generators for supplying a-c auxiliary power
	2. Optimum operating flexibility	2. Availability of propulsion generators for supplying a-c auxiliary power	2. Superior maneuverability over turbine geared drives
	3. Adaptability with multiple Diesel engines per shaft	3. Superior maneuverability over Diesel geared or direct drives	3. Greater over-all efficiency at cruising speeds and better operating flexibility than turbine geared drives in twin screw vessels
	4. Availability of propulsion generators for supplying d-c auxiliary power	4. Greater reliability, less maintenance and lower space and weight requirements than d-c Diesel-electric driven	4. Higher capacity range than plants with Diesel engine prime movers
Most probable naval surface vessel applications	Ice breakers (AG), minesweepers (AM), net laying ships (AN), salvage ships (ARS), rescue ships (ASR), gasoline tankers (AOG), ocean tugs (ATA, ATF), harbor tugs (YTB, YTM, YTL), fuel oil barges (YO), ferry boats	Destroyer escorts (DE), submarine tenders (AS)	Destroyer escorts (DE), cargo and troop transports (AP, APD, APA, AKA), oilers (AO)

* An exception to this limit is the use of d-c Diesel-electric propulsion in ice breakers, which require capacities as high as 5,000 shaft horsepower per shaft.

** The upper capacity limit for a-c Diesel-electric plants is based upon the use of not more than five engines per shaft, and engine capacities not exceeding about 2,400 brake horsepower.

construction and installation of electric propulsion equipment for naval service in order to insure a high degree of reliability and good performance without requiring excessive maintenance or attention. Because of the urgency of wartime requirements and the lack of basic naval specifications, most of the electric propulsion equipment provided for the earlier applications was essentially of commercial design. However, improvements in design, installation and operating practices were effected in later installations. Naval basic specifications[7,8] covering the construction and installation of electric propulsion equipment are now available and will be revised periodically as additional experience is acquired. Accordingly, the Navy is more adequately prepared for future applications of electric propulsion in naval surface vessels than in the past. However, it is expected that future applications will be determined primarily by engineering and performance considerations, particularly during peacetime, rather than by procurement considerations, as was frequently the case during the recent war. Based upon engineering and performance considerations, the most probable fields of application of the three types of conventional electric drives in naval surface vessels may be summarized as in Table III.

It should be noted, however, that electric propulsion must compete with other developments in the field of marine propulsion, such as the development of reliable controllable and reversible pitch propellers, improved types of reversible reduction gears and clutches, and lightweight planetary gears. Consequently, it is important that the development of electric propulsion equipment toward obtaining more reliable, lighter and more compact equipment be continued. Along these lines, the further simplification and development of control equipment to withstand high impact shock, the incorporation of space and weight saving materials and construction features, and the use of high-temperature insulating materials, such as those utilizing silicone varnishes and impregnants, are important. If interest in electric propulsion is to be maintained in the face of developments in other fields, it is essential that the further development of electric propulsion equipment be carried out during peacetime, despite the relatively small present naval demand for such equipment.

Some of the more recent developments which should serve as incentives toward extending the use of electric propulsion in naval surface vessels may be noted.

1. Development of gas turbines for marine propulsion. Since gas turbines are essentially high-speed, undirectional, non-self-starting prime movers, and must be operated at constant speed or over a limited speed range for efficient service, they require a transmission system which will provide speed reduction and overcome the deficiencies of gas turbines for propulsion service. Electric propulsion is probably the most promising transmission system available at present for this purpose. In this connection, d-c electric drives have inherently the best operating characteristics for use with gas turbines because of the variable speed transmission ratio obtainable with d-c machines. On the other hand, because of the high prime mover speeds and the higher capacities in which gas turbines appear most promising, a-c electric drives permit considerable reduction in weight and space, as well as increased reliability, as compared with d-c electric drives.

In order to overcome the fixed speed transmission ratio inherent in a-c synchronous electric drives and thereby make them more adaptable for use with gas turbines, the Bureau of Ships and several manufacturers are developing an a-c electronic propulsion system which appears to offer promising possibilities. In the a-c electronic system synchronous machines of essentially conventional design are employed. However, the a-c power from the generators is supplied to a cyclo-converter, which is essentially a frequency changer. The cyclo-converter contains 18 sealed mercury pool-type power tubes. By control of the supply to the grids and igniters of these tubes, it is possible to vary the frequency of the a-c power output from the cyclo-converter continuously over the entire operating range, independently of the input power frequency. In addition, the phase sequence of the output power can be reversed. The electronic propulsion system therefore should provide the same variable speed transmission ratio as can be obtained with d-c electric propulsion, while still retaining the advantages inherent in the use of a-c machines. It is anticipated that a detailed description of this system and of its performance will be published at some future date.

2. Trend toward higher speed steam turbines and other high-speed prime movers. Prime mover speeds well in excess of 5,000 rpm may be expected in the future. The greater speed reduction required with these prime movers will probably necessitate the use of gears with three stages of speed reduction when geared drives are used. The increased size and weight of such gears may be such as to at least partially cancel the present weight and space superiority of geared drives over electric drives of comparable capacity in such applications.

3. Increased importance of reducing the noise level of the propelling plant to a minimum in certain classes of naval vessels. The principal sources of noise are the propellers and reduction gears. Propeller noise can be minimized by careful attention to propeller design and by using lower speed propellers. However, lower speed propellers necessitate the use of reduction gears with higher gear ratios, which in turn increases the noise level of the gears. While considerable work is being carried on in an attempt to develop more quiet reduction gears, nevertheless the inherent quietness of the electric drive utilizing slow-speed propulsion motors makes this drive very attractive where extreme quietness of operation is desired.

4. Trend toward ever increasing capacities of propelling plants for certain types of naval vessels. With increased plant capacities, the problems connected with the arrangement of machinery become increasingly difficult. Also, the number, weight, and length of propeller shafts increase. Considerations of vulnerability and weight and space requirements for shafting may lead to reconsideration of electric propulsion in naval vessels with propelling plants of large capacity, since the propulsion motors may be placed well aft in the ship, thereby reducing the length and size of the shafting.

Conclusions

It is hoped that the preceding discussion has served to acquaint the reader with the background and present status of electric propulsion in the United States Navy, and that it will stimulate further interest in this field. Space limitations and other considerations have prevented the inclusion in this paper of detailed data relative to the performance of the several types of electric drives, particularly as compared with other types of drives. However, it is anticipated that additional publications will be released to supplement this article so that equipment manufacturers and ship operators may benefit from the Navy's experience with electric propulsion in naval surface vessels.

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Discussion

H. C. Coleman (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The United States Navy pioneered in furthering the development and application of electric propulsion equipment for ships. This participation in and encouragement of such application has been of benefit in adopting this form of drive to many types of merchant vessels. In view of the outstanding interest and experience of the Navy in electric propulsion, it is not surprising to find that there were more than 1,100 surface vessels equipped with electric propulsion in the last ten years. This paper gives an interesting and valuable summary of the various types of drives applied on these vessels.

The description of the a-c Diesel-electric drive on the submarine tender *Sperry* is a good example of the Navy's pioneering. The experience gained on this installation should lead the way to the utilization of this type of drive on certain large merchant vessels.

The author indicates that with the drive such as used on the *Sperry* the propeller cannot be operated continuously at speeds below 30 per cent of rated speed without the addition of special equipment. This is true, but special equipment is rather simple. It is possible to operate the synchronous motor as a squirrel-cage induction motor by proper adjustment of the generator excitation below its normal value and with the motor field connected to a resistor. Thus, very little extra equipment is involved.

The author states that the maximum motor torque which can be developed during starting and reversing is limited to not more than about 110 per cent of rated torque. Actually, such a high torque is not realized. Under the maneuvering condition the engine speed is reduced to about 30 per cent, and at this speed the engine will not produce more than about 85 per cent torque. With the conversion factor through the electrical transmission the maximum motor torque available becomes about 60 per cent. However, with proper arrangement for dynamic braking on the motor, satisfactory maneuvering can be obtained.

The author refers to the development work which is being carried out in conjunction with an electronic propulsion system which was considered primarily for use with gas turbines. It is true that the cycloconverter or frequency-changer provides an almost ideal means of controlling the speed and direction of rotation of the propulsion motor without affecting the prime mover; the complications and problems encountered in accomplishing this result are very considerable. The work which has been carried out up to the present time would seem to indicate serious handicaps in size, weight, complication, and reliability when compared with the presently used electric transmission systems.

W. E. Jacobsen (General Electric Company, Schenectady, N. Y.): The paper by Jucarone is directed to a consideration of the different types of electric drives used by the Navy in ships built during the last war, and reviews certain associated application problems. The more controversial field of advantages and disadvantages of electric drive is purposely left to previous literature.

It is readily apparent that the author's choice was a favorable one, and it is equally apparent that he has presented a well-rounded and coherent story of electric drive in modern naval vessels. This discussion will therefore be brief, commenting on only a few items.

The trend to totally enclosed electric propulsion machinery is noteworthy. The Navy has apparently found the added advantages of totally enclosed machinery worth the additional cost. The advantages of this type of machinery are considerable and might well be commended to operators of other classes of vessels. The additional cost is not a large percentage of the total, particularly if proper allowances are made for the corresponding cost of the room ventilating system which must otherwise be provided to secure adequately low ambient temperatures. Too often this compensating economic factor is not worked into the comparison properly.

The Navy's favorable experience with an a-c Diesel electric drive is most interesting. Here in spite of the inherently poor torque conversion during maneuvering when the propeller motor must be operated on its amortisseur winding, it was found possible to make successful reversals with only 50 per cent of the engines in service. This is an important feature in an a-c Diesel electric drive which is generally a multi-engine system, and which may often be operated with less than the full number of engines in service.

The building by the Navy of the same type vessels employing d-c Diesel-machinery on some and a-c Diesel on another, provides one of those rare opportunities for making a dependable comparison. The fact that the a-c machinery installation weighs two-thirds as much, has less than half the losses, and involves less maintenance than the corresponding d-c machinery installation makes its use attractive in spite of certain fundamental disadvantages. The author's conclusion that the use of a-c Diesel electric systems is attractive on ships of medium and higher rated power where maneuvering and towing characteristics are not of primary importance, seems well founded. It remains to be seen, however, whether or not the higher first cost of this system over a nonelectric drive system will permit its use in more than a limited number of vessels.

N. T. Jucarone: Coleman has pointed out that continuous propeller speeds below 30 per cent of rated speed can be obtained in a-c Diesel electric drives by reducing the generator field excitation below normal and operating the propulsion motor as an induction motor. During the trials of the *Sperry*, portable apparatus was installed and continuous operation at propeller speeds down to about 7 per cent of rated speed was found possible when all four generators were supplying power to one shaft. The limitation on minimum speed was determined by the fact that at very low values of generator field excitation, the generators hunted because of the low synchronizing torque.

Jacobsen stressed the trend toward the use of totally-enclosed water-cooled propulsion machines in naval ships. For further discussion on difficulties in providing adequate ventilation and protection against damage from water where open and semi-enclosed machines have been used, refer to the discussion by W. H. Fifer on a recently published AIEE paper.[1]

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Overload Protection of Silicone-Insulated Motors for Shipboard Use

J. C. GRIGG
ASSOCIATE AIEE

J. E. WATKINS
NONMEMBER AIEE

AT THE present time, the Bureau of Ships uses overload relays mounted in the controller enclosure as a means of protecting motors from overload, single-phase, and locked-rotor conditions. Both magnetically actuated and thermally actuated relays are used. These overload relays have not been considered too satisfactory for motor protection for a number of reasons. Some of these reasons are as follows:

1. The trip curve of the overload relay does not follow the thermal curve of the motor. The discrepancy in these curves may cause the overload relay to trip before the motor has reached its allowable temperature rise. This is especially true in starting motors with a high torque load which results in a relatively long accelerating period. At other points along the curve, conditions of inadequate protection exist.

2. If the motor and controller are located in different ambient temperatures, as is usually the case aboard ships, the use of overload relays compensated for changes in ambient temperature is necessary. If the motor and controller were located in the same ambient temperature, noncompensated overload relays should be used for the most adequate overload protection to the motor. In order to cut down on the number of types used, all overload relays are presently compensated, thereby sacrificing some motor protection in order to obtain greater standardization.

3. The calibration and rating of the overload relay can be easily tampered with and changed. This provides the operating personnel with a very tempting method of stopping the relay from tripping out on relatively small overloads rather than attempting to locate the actual cause of the trouble. It has been necessary from a manufacturing standpoint to make the overload relays so that the rating can be easily changed because of the fact that a large number of heater coil elements are required for each relay in order to approximate the heating curve of the motors. Even with a large number of overload coils, the relays frequently have to be adjusted after they are installed.

With the advent of silicone-insulated motors requiring different overload relay characteristics than previously encountered, it was considered an opportune time to develop a more effective and satisfactory means for protecting motors from overloads. In protecting motors, the thing which has to be protected against is high temperature. Therefore, it is obvious that the most direct method of protecting a motor against high temperature is a device which will operate directly from the heat of the motor.

Devices which operate from the heat of the motor have been used by a number of manufacturers for fractional horsepower motors with varying degrees of success. With this in mind, the Bureau embarked on a program for the development of thermostatic protectors for a-c silicone-insulated motors.

Specification Requirements

In setting up the development program for a thermostatic protector, the following operational requirements which the protector would be required to meet were specified. These values were based on the characteristics of available motors, the fact that a silicone-insulated motor can operate continuously at 200 degrees centigrade and can operate for a short time at temperatures up to 350 degrees centigrade.

1. With the rotor stalled before the voltage is applied, the protector shall trip before the temperature of the motor windings reaches 350 degrees centigrade when measured by the rise-in-resistance method.

2. With the rotor stalled by an overload after the temperature of the motor windings has reached a steady-state condition of 200 degrees centigrade, the protector shall trip before the temperature of the motor windings reaches 350 degrees centigrade when measured by the rise-in-resistance method.

3. With a 25-per cent overload applied after the temperature of the motor windings has reached a steady-state condition of 200 degrees centigrade, the protector shall trip within 15 minutes after the motor windings reach a temperature of 210 degrees centigrade measured by the rise-in-resistance method.

4. With a 50 per cent overload applied after the temperature of the motor windings has reached a steady-state condition of 200 degrees centigrade, the protector shall trip within six minutes after the motor windings reach a temperature of 210 degrees centigrade when measured by the rise-in-resistance method.

5. With an 80 per cent overload applied after the temperature of the motor windings has reached a steady-state condition of 200 degrees centigrade, the protector shall trip within two minutes after the motor windings reach a temperature of 210 degrees centigrade measured by the rise-in-resistance method.

6. The protectors were otherwise required to meet the specifications for conventional overload relays.

Equipment Description and Test Results

The Bureau of Ships has been working with three manufacturers in the development of thermostatic protectors. These manufacturers are the Spencer Thermostat Company, the Radio Frequency Laboratories, Incorporated, and the White Rodgers Electric Company. The results obtained up to this time indicate that suitable protection for squirrel-cage a-c motors can be obtained by means of a thermostatic device which is relatively simple and reliable.

The thermostatic protector developed by the Radio Frequency Laboratories,

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J. C. Grigg and J. E. Watkins are both with the Bureau of Ships, Navy Department, Washington, D. C.

Incorporated (see Figure 1) consists basically of a thermosensitive element which is inserted in the stator slot along with the motor windings and trip mechanism for breaking the control circuit to stop the motor.

The thermosensitive element is based on the difference in rate of thermal expansion of dissimilar metals. Invar is a metal with a very low rate of expansion; whereas steel expands considerably when heated. An invar rod is placed inside a steel tube with a stop in one end and the trip mechanism at the other end. When the thermosensitive element is heated, the steel tube becomes longer and the invar rod becomes loose. When this element is cooled, the steel tube becomes shorter causing the invar rod to exert a force against the trip mechanism. This force is used to operate the trip mechanism.

The trip mechanism changes this large force and small movement into a smaller force with a larger movement by applying the force from the thermal element to the outer edges of a cup-shaped disk. This force is applied in such a way as to cause the disk to snap inside out at a predetermined pressure. When pressure is released, the disk snaps back to its unstressed position. A small plunger is attached to the center of this disk to operate the contacts. The contacts consist of two stationary contacts and one movable contact. The movable contact is held closed by means of a spring and is opened by the plunger striking it when the disk snaps back into its unstressed position.

In mounting this protector in the motor, the thermosensitive element was inserted in the motor slot between two coils so that there was as small a differential of temperature as possible between the thermosensitive element and the motor windings. The trip mechanism was fastened to the motor frame, and the leads were brought out through the motor terminal box for connection to the control circuit.

In testing this thermostatic protector, it was installed in a silicone-insulated motor (see Figure 2) and tests were conducted to determine whether it met the requirements previously indicated. In addition to these requirements, tests were also conducted to determine if the motor was adequately protected against single-phase conditions. Typical results obtained in these tests are shown in Table I.

The thermostatic protector developed by the White Rodgers Electric Company consists of a sensitive bulb 18 inches long with an inside diameter of 0.120 inch and outside diameter of 0.156 inch. (See Figure 3.) This bulb is filled with a liquid which expands with increase of temperature. This bulb is connected by means of a capillary tube to a diaphragm. On an increase in temperature, the diaphragm moves to open the contacts. The assembly of the contact-making portion of the device is such that snap action of the contacts either to open or close position occurs at a predetermined diaphragm position.

The sensitive bulb was wound around the end turns of the motor winding and securely fastened to the windings by means of insulating tape. The capillary tube was brought out through the motor terminal box to the switch mechanism mounted adjacent to the terminal box. It is expected that the mounting of the switch mechanism inside the terminal box will be feasible for future designs.

Tests as indicated above for the Radio Frequency protector were also conducted on the White Rodgers protector using identical motors. Typical results obtained in these tests are given in Table II.

Table I

	Starting Temperature, Degrees Centigrade	Time to Trip, Minutes: Seconds	Temperature at Trip, Degrees Centigrade	Temperature at Resetting, Degrees Centigrade
25 per cent overload from full load	200	3:05	222	173.5
50 per cent overload from full load	200	0:52	227	175.5
80 per cent overload from full load	200	0:35	225	172.2
Locked rotor from full load	200	0:26	241	172.2
Locked rotor with motor cold	20.5	3:25	264	175.0
Single phase from full load	200	1:12	258	175.0

The thermostatic protector developed by the Spencer Thermostat Company is of the thermostatic disk type (see Figure 4). Each unit consists of a molded glass silicone base on which the contacts, bimetal disk, and a heater are mounted. The protector has two poles with one pole being operated by the tripping element and the other pole being operated by a mechanical linkage between the two poles.

The bimetal disk is cup-shaped and designed so that when it reaches a certain temperature it will snap inside out due to the difference in expansion of the metals. When the disk snaps inside out, it opens the contacts and stops the motor. A heater coil, through which the line current passes, is mounted directly under the disk so that its heat is transmitted to the disk.

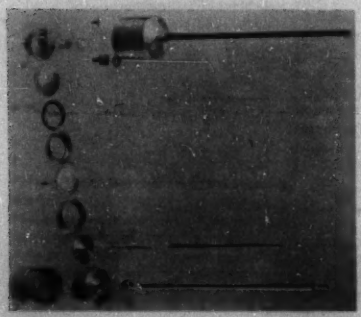

Figure 1 (left)

Figure 2 (right)

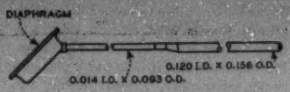

Figure 3 (left)

Figure 4

The protector is mounted in the motor as close to the winding as possible so that it is at the same ambient temperature as the motor. On small overloads, the design is such that tripping depends almost entirely on the change in ambient temperature due to the increased temperature of the motor windings. As the amount of the overload increases, the heat due to the current through the heater has more effect on the tripping time until at locked rotor the tripping is due almost entirely to the heat from the heater. Two protectors were mounted in the motor in order to insure satisfactory protection on single-phase conditions. Typical results obtained with a motor on which this type of protector was installed are shown in Table III.

Conclusion

In comparing the thermostatic protectors just described, each has certain advantages and disadvantages. The protector developed by the Radio Frequency Laboratories has the advantage of being imbedded in the windings in the motor slot so that it will follow the motor heating curve very closely. However, the disadvantage is that it will probably require that one slot of the motor be made larger than the other slots due to the extra space required. It is also considered doubtful if this protector can be applied successfully to very small motors since a definite movement of the tube due to thermal expansion of steel is required. This fact fixes the tube length for a given temperature rise.

The protector developed by the White Rodgers Electric Company has the advantage of ease of installation. However, it has the disadvantage of having some time lag between the actual motor temperature and the temperature of the protector.

The protector developed by the Spencer Thermostat Company has the advantage of having very fast action on high values of overload or on locked-rotor conditions. In fact, the tripping time on the model tested should be increased to allow full utilization of the motor. This protector, however, has the disadvantage of partially depending on motor current for protection. This will result in the requirement of current transformers for large sizes of motors and may result in a large assortment of heater coils being required to cover the entire range of motor ratings.

From the foregoing tests, it can be seen that it is possible to protect a squirrel-cage induction motor from overloads, single-phasing and locked-rotor conditions by means of thermostatic protectors.

There are some disadvantages in the use of thermostatic protectors. One disadvantage is that control wiring must be run to the motor. Also, space must be found inside the motor enclosure for the thermally sensitive device. In addition, the reset time of the motor protector is greater than that of the conventional thermal overload relay. The first disadvantages can be partially overcome by the use of multiconductor cables to eliminate extra control wires. With connections for the protector brought out to the motor terminal box, extra motor wiring during installation will not be a particular hardship. Since, as indicated herein, protectors of small physical size can be built, it is believed that if the use of a protector is considered in the original design of the motor, the finding of space for the protector will not noticeably penalize the design of the motor. It is felt that in applications where the reset time lag cannot be tolerated, emergency operation of the motor can be obtained by by-passing the thermostatic protector contacts.

These disadvantages are considered to be outweighed by the more adequate protection afforded silicone-insulated motors against overheating due to any cause. Furthermore, it is considered that fewer types of devices can be used to protect

Table II. Protector Set to Trip at 210 Degrees Centigrade

	Winding Starting Temperature, Degrees Centigrade	Time to Trip, Minutes:Seconds	Winding Temperature at Tripping, Degrees Centigrade	Temperature at Resetting, Degrees Centigrade
10-per cent overload from full load	200	5:18	212	204
25-per cent overload from full load	200	1:45	218	203
50-per cent overload from full load	200	1:20	229	203
80-per cent overload from full load	Motor would stall so that this test could not be run			
Locked rotor from full load	200	0:30	264	203
Locked rotor with cold motor	27	3:05	330	203
Single-phase motor at full load, phase *A* open	200	1:50	230	203
Single-phase motor, cold, locked rotor, phase *A* open	27	6:00	265	203
			Phase BC	

Table III

	Starting Temperature, Degrees Centigrade	Time to Trip, Minutes:Seconds	Temperature at Tripping, Degrees Centigrade	Temperature at Resetting, Degrees Centigrade
25-per cent overload from full load	190	5:00	203	174
50-per cent overload from full load	190	1:39	202	174
100-per cent overload from full load	190	0:21	200	173
Locked rotor from full load	190	0:03	200	172
Locked rotor with motor cold	25	0:11	126	126
Single-phase motor, cold, open phase *A*, 50-per cent overload	25	7:24	192	173

the many ratings and sizes of motors used by the Navy.

The applying of thermostatic protectors to class-*A* insulated motors presents additional problems since the smaller range of operating temperature requires more sensitivity and speed of response. However, it is believed that adequate protection for such motors can be obtained using designs similar to those indicated above.

The Bureau of Ships is presently applying a number of thermostatic protective devices to silicone-insulated motors aboard vessels to determine their adequacy under actual operating conditions. It is expected that if data being obtained prove satisfactory, these protectors will be used in future ship designs.

Discussion

V. G. Vaughan (Spencer Thermostat Company, Attleboro, Mass.): I am particularly interested in the comment regarding the disadvantages of the inherent overheat protector that must respond to motor current.

In order to protect the motor when the rotor is locked during high running overloads, it is necessary to get heat into the protector faster than the normal flow of heat from the copper to the motor winding through the insulation to the protector. When current flows through the protector, this current furnishes heat to the protector so that the protector heats at the same rate as the motor winding. I, therefore, consider it a definite advantage to have the motor current flow through the protector.

Today we have learned some of the underlying fundamentals that allow us to design protectors that will meet almost any condition. By that, I mean we can control the locked rotor characteristics independent of the running loads, and correspondingly, we can control the overload characteristics independent of either locked rotor or running loads. On ultimate trip we might have a motor cutoff at 200 degrees centigrade and on overload we can arrange it so that on high overload we might cutoff as low as 170 degrees centigrade, or we might design the protector so that it would allow the motor to carry high overloads for short time and not cutoff before it attained temperatures of 210 degrees. These devices are not available for these high temperatures. As a matter of fact, they are not necessarily available in all ratings for 125 degrees centrigrade, (Class "A" insulation limits) but as indicated we have learned some of the underlying principles which lead us to believe that we can accomplish these ends.

J. C. Grigg: It is agreed that by using the protector as developed by the Spencer Thermostat Company, it is possible to vary the winding temperature on which the protector operates on high overloads by the use of different heater coils. However, the disadvantage of using heater coils in this device is that for large size motors, current transformers will be required in order to keep the current through the heater at a reasonable value. Also, the use of heaters would probably result in a very large number of heater coils being required to cover the entire range of motor ratings and types. This large number of heaters would complicate the stocking and supply problem of overload protectors considerably.

Effective Resistance and Inductance of 3-Conductor Shipboard Power Cables

SAMUEL D. SUMMERS
MEMBER AIEE

Synopsis: The effective resistance and inductance of 3-conductor shipboard power cables are measured at frequencies up to 800 cycles per second. The effects of conductor spacing, steel armor, and adjacent magnetic materials are described. The results indicate that the effective resistance is increased greatly as the conductor separation is reduced and that both the steel armor on the cables and other adjacent magnetic materials increase the effective resistance and inductance of the cable.

IN studying the general problem of weight reduction of shipboard equipment, investigation was made of the advantages of higher frequency electric power. A study of the transmission characteristics of 3-conductor power cable was made as a part of this investigation. The characteristics of particular interest which are the subject of this paper are the effective resistance and inductance of conductor cables when used in phase circuits.

In an isolated conductor carrying alternating current, the skin effect causes a nonuniform current distribution which is symmetrical with respect to the center of the conductor. However, if the conductors of the circuit are in close proximity as in the case of a multiconductor cable, there results an asymmetrical distribution of current. For example, in a return circuit of two closely spaced parallel conductors, the current density is increased on the sides of the conductors nearest each other. This proximity effect causes an increase in the effective resistance and a decrease of the effective inductance of the conductors forming the circuits. These effects are functions of the size and spacing of conductors and of the frequency of the currents.

Formulas which take into account this proximity effect have been derived for the effective resistance and inductance of two closely spaced parallel round wires in a single-phase circuit,[1,2] and for three equally spaced round wires in a 3-phase circuit.[2,3] However, their application is laborious and the accuracy when applied to a power cable of stranded conductors is questionable, especially since it is known also that these characteristics are affected by the armor and shielding on the cable, by adjacent conducting and magnetic materials and by the spiralling of strands and conductors.

The proximity effect in conductors on a single-phase circuit was studied experimentally by A. E. Kennelly, F. A. Laws and P. H. Pierce[4] in 1915 as a part of a general investigation of skin effect in conductors. The proximity effect in several 3-conductor underground power cables was measured by E. H. Salter, G. B. Shanklin and R. J. Wiseman[5] in 1934. However, it is evident that the effective resistance and inductance of 3-conductor cable are dependent upon so many factors that extrapolation of data from one type and size of cable to another is of questionable accuracy and would perhaps lead to serious error. For these reasons, direct measurement of the effective resistance and inductance was judged necessary to obtain the desired characteristics of the cables.

Method of Test

Since the effective resistance and inductance of a 3-conductor cable depend upon the phase relation of the currents in the conductors, it is necessary that the test circuit make use of balanced 3-phase currents. The circuit arrangement and the method of measurement used in the tests reported here are similar to that described in reference 5. The circuit used is shown in Figure 1.

As a maximum frequency chosen for this investigation was 800 cycles per

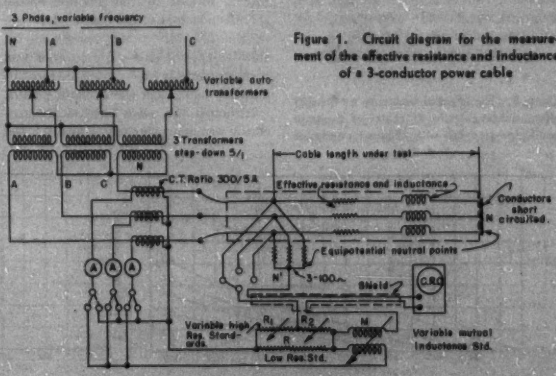

Figure 1. Circuit diagram for the measurement of the effective resistance and inductance of a 3-conductor power cable

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SAMUEL D. SUMMERS is with the Naval Research Laboratory, Washington, D. C.

Table I. Description of Cables

Cable Designation	Number of Conductors	Conductor Area, Cir. Mils	Strands Per Conductor	Separation of Conductor, Inches	Outside Diameter of Cable, Inches	Cable Covering
A	3	158,660	61	0.129	1.45	Al tape (0.004 inch) under synthetic rubber
B	3	158,660	61	0.125	1.45	Braided steel armor
C	3	158,660	61	0.109	1.32	Braided steel armor
D	3	158,660	61	0.121	1.47	Braided steel armor
E	3	158,660	61	0.110	1.50	Semiconducting paper under synthetic rubber
F	3	59,200	37		1.04	Al tape (0.004 inch) under Al armor
G	3	405,620	127		2.20	Braided steel armor
H	6	59,200	37		1.45	Al tape (0.004 inch) under Al armor

second, one of the difficulties encountered was obtaining a suitable power source of adequate current capacity. After some preliminary experimentation, it was found that both the effective inductance and resistance were independent of cable current except for a slight saturation effect in the steel armor of the cable and in adjacent magnetic materials. For this reason, and because of the limitation in current capacity of the available equipment, the cable currents used in these tests were less than the normal rated currents of some of the larger cables tested.

The source of voltage selected was a 3-phase 208/120-volt 9-kva variable-speed (4,000–8,000) rpm aircraft alternator. The voltage was stepped down through three Y-connected variable ratio autotransformers, rated 110-volt, 5-kva 60-cycle, and a bank of three Y-Y connected transformers rated 25-kva 575–115 volt, 60-cycle. The low-voltage output of these transformers was applied to one end of the cable, the conductors at the other end being connected together with a special low resistance connector to form a Y-system. The line currents were metered through three current transformers. Balanced line currents were obtained by adjusting the auto transformers.

Cable test lengths were approximately 25 feet and were supported on a wood platform 18 inches above a concrete floor except where otherwise noted. A potential stud was attached to each of the conductors near the source end of the cable, shielding and armor were carefully removed from the area around the stud to avoid electrical connection with it.

The effective resistance and inductance of each conductor length between the potential stud and the short circuiting connector were measured by means of a bridge-like circuit, by comparing the resistive and reactive component voltage drops in the conductor with the voltage drops produced by the secondary current of the current transformer flowing through a standard noninductive variable resistance and a standard variable mutual inductance respectively. For the convenience of the reader the circuit theory and the derivation of formulas used are given in the Appendix

In order to avoid stray induced voltages in the detector circuit of the bridge, the detector circuit was returned to a neutral created by three 100-ohm Y-connected resistors rather than to the neutral formed by the junction of the three conductors of the cable. All leads in the detector circuit were made of shielded twisted pair and precaution was used in arranging leads physically to avoid induced voltages in the detector circuit.

Although the wave form of the generator was approximately sinusoidal, the measurement of the effective resistance and inductance was not affected by any distortion present in the voltage since the bridge balance was obtained for the fundamental frequency only.

The metering circuit was switched into the secondary of each current transformer in turn and the effective resistance and inductance of each conductor of the cable were thus obtained. The direct current resistance of the conductors of the cable was measured by means of a Kelvin double bridge.

Results of Tests

The effective resistances of the three conductors of the cables were generally found to be unequal. This inequality seemed to result from the unequal spacing of the conductors in the cable. This conclusion is borne out by the fact that the conductor separation measured from several cable sections showed considerable inequality and by the results of this investigation which indicate that the effective resistance is critically affected by conductor spacing.

Therefore, for the purpose of comparison, the values of effective inductance and the skin effect resistance ratio given herein represent the averages for the three conductors of the cable. In order to make comparison of effective resistance of the cables of different sizes and construction under the several conditions of test, the skin effect resistance ratio R/R_o is used in this paper rather than the ohmic value of the effective resistance. Although the usual increase in skin effect resistance ratio with increased frequency is noted in this report, attention is directed to the proximity effect of conductors and to the effect of armor and adjacent magnetic materials on both the skin effect resistance ratio and the effective inductance.

The pertinent information concerning the cables tested is listed in Table I. Ref-

Figure 2. The effect of frequency on the skin effect resistance ratio of cables of the same conductor size but with different conductor separation

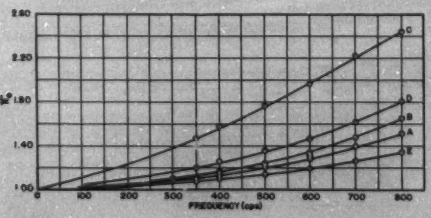
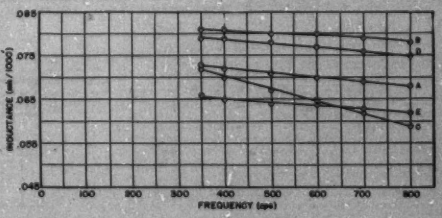


Figure 3. The effect of frequency on the effective inductance of cables of the same conductor size, but with different conductor separation



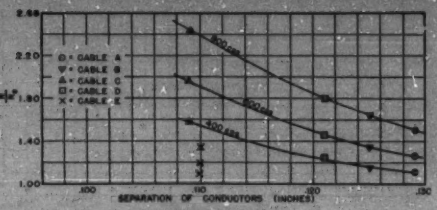


Figure 4. The effect of conductor separation on the skin effect resistance ratio for cables of the same size

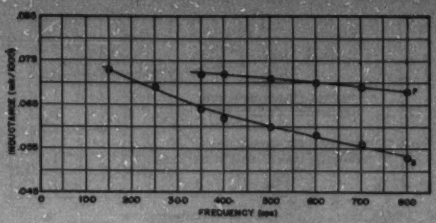


Figure 6. The effect of frequency on the effective inductance of cables of different size

erence to the particular cables described in this table is by the cable designation A, B, C and so forth.

The conductors of cables A, B, C, D, and E are of one size except for minor differences due to manufacture. However the differences in conductor separation, shielding, armor and, in one cable, to the use of strand interstitial material cause a wide variation in the effective resistance and inductance of these cables. This variation is shown in Figures 2 and 3. Primary among these factors which control the effective resistance and inductance is the conductor separation. The effect of conductor separation on the skin effect resistance ratio is shown in Figure 4, in which it will be noted that the proximity effect of small separation causes a very high ratio.

It will be seen in Figure 4 that although cable E has a very small separation of conductors, the skin effect ratio is extremely low. This apparent anomaly is explained by the fact that the particular strand interstitial material, a rubber-like synthetic, actually serves as a partial insulator between adjacent layers of strands so that the conductor behaves as if the individual strands were coated with a poor insulator. This partial insulation was verified by a continuity test between strands on short lengths (two feet) of the cable. A complete separation of interlayer strands would tend to reduce the proximity effect which would result in lower effective resistance. While a small separation of conductors causes the effective resistance to increase rapidly as the frequency increases, it also causes the effective inductance to decrease as the frequency increases. See the curves of cable C in Figures 2 and 3.

The skin effect resistance ratio versus frequency for cables F and G are shown in Figure 5. These cables differ greatly in area and the curves serve to illustrate the effect of conductor size on the effective resistance of a cable.

To illustrate the magnitude of the proximity effect in 3-conductor cables, the skin effect resistance ratio for an isolated solid conductor of an area equivalent to that of a conductor of cable G is shown by curve I in Figure 5, the data for which were calculated by well-known methods. The difference between this curve and that for cable G is due largely to the proximity effect and in a minor way to the effect of the steel armor on the cable.

The effective inductance of cables F and G are shown as function of frequency in Figure 6. Here it will be observed that the inductance of the large cable falls off rapidly as the frequency increases.

The characteristics of a 6-conductor cable when carrying 3-phase currents were measured during this investigation. The two symmetrical arrangements of conductors used in the tests were:

1. Two adjacent conductors in parallel.
2. Two diametrically opposite conductors in parallel.

The skin effect resistance ratio for these two arrangements of conductors is shown in Figure 7 as a function of frequency. It will be noted that the skin effect resistance ratio is considerably lower when the diametrically opposite conductors are operated in parallel.

The effective inductance for these two arrangements is shown in Figure 8 as a function of frequency. From these curves it will be seen that with the diametrically opposite conductors in parallel the effective inductance is approximately one-half as great as when the adjacent conductors are in parallel.

A comparison of these results with characteristics of 3-conductor cables indicates that a saving in copper and improved line regulation can be obtained by using a 6-conductor cable on 3-phase operation instead of the conventional 3-conductor cable. This improvement increases with the frequency.

Figures 9 and 10 show the effect of ad-

Figure 5. The effect of frequency on the skin effect resistance ratio for cables of different sizes

Note: Curve I shows the skin effect ratio for an isolated conductor of area equal to a conductor of cable G

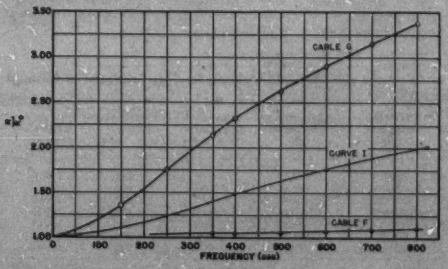
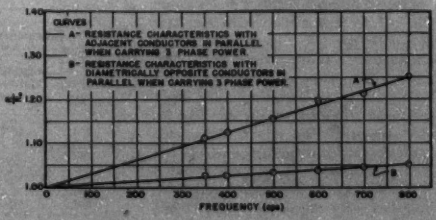


Figure 7. The effect of frequency and conductor grouping on the skin effect resistance ratio of a 6-conductor cable when used in a 3-phase power circuit



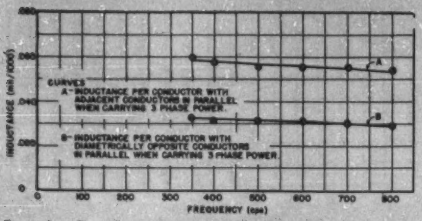


Figure 8. The effect of frequency and conductor grouping on the effective inductance of a 6-conductor cable when used in a 3-phase power circuit

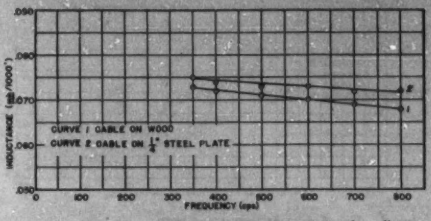


Figure 10. The effect of adjacent magnet material on the effective inductance of an unarmored cable

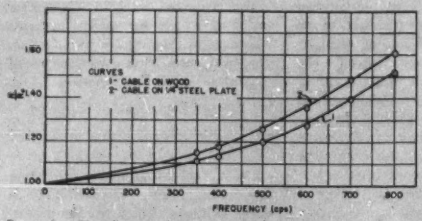


Figure 9. The effect of adjacent magnetic material on the skin effect resistance ratio for an unarmored cable

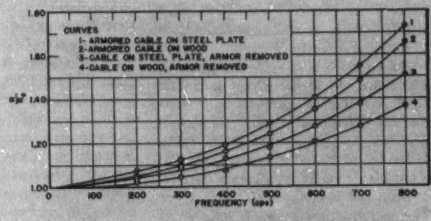


Figure 11. The effect of steel armor and other adjacent magnetic material on the skin effect resistance ratio of a cable

jacent magnetic materials on the effective resistance and inductance of cable A, which is unarmored but has an aluminum shield under the cable sheath. These curves show that both the effective resistances and inductance are increased by the cable being in contact with a steel plate. This is due to the eddy current losses in the steel plate being reflected into the conductor and in the case of the effective inductance to the fact that the steel plate increases the flux linkages of the conductors.

Figures 11 and 12 show the effect of adjacent magnetic material on the effective resistance and inductance of an armored cable and the effect of the armor itself on these characteristics. In these figures the difference between curves 1 and 2 shows the increase in skin effect resistance ratio and the effective inductance due to the armored cable being placed on a 1/4-inch steel plate. The difference between curves 3 and 4 shows the increase in skin effect resistance ratio and in the effective inductance due to the unarmored cable being placed on the steel plate. The difference between curves 2 and 4 shows the decrease in skin effect resistance ratio and in the effective inductance when the armor is removed while the cable is supported on wood. The difference between the curves 1 and 3 shows the decrease in skin effect resistance ratio and in the effective inductance when the armor is removed while the cable is supported on a 1/4-inch steel plate.

Figures 9, 10, 11 and 12 indicate that both steel armor and other adjacent magnetic materials increase the effective resistance and inductance of a 3-conductor cable.

Accuracy of Measurement

The accuracy of the measurement of the effective resistance and inductance depends upon the accuracy of the standard resistances, the standard mutual inductance and the ratio of the current transformer in the circuit. The precision of the instruments used assures an error not greater than 1 per cent in the measurement of effect resistance. Although the detector circuit was capable of a precise balance, inability to read the scale of the inductometer accurately resulted in a larger error in the effective inductance. Because extraneous induced voltages in the detector circuit tend to create a false balance and therefore an error in both effective resistance and inductance, care was used in locating equipment and circuit leads to avoid this source of error.

As a check on the accuracy of measurement, the voltage drop in the conductors of cables was measured with a special calibrated voltmeter and this voltage compared with the calculated impedance drop in the conductor. The result of such measurement for cable G is shown in Table II.

Despite the fact that errors in the measurement of effective inductance appear almost directly as errors in calculated voltage as a result of the low power factor, the reasonable agreement between measured and calculated voltages would indicate that a reasonable accuracy of measurement of both effective resistance and inductance obtains.

Table II. Comparison of Calculated and Measured Voltages for One Conductor of Cable G

Frequency, cps	Cable Current, Amperes	Effective Resistance, Ohms	Effective Inductance, (mh)	Calculated Impedance Drop, Volts	Measured Voltage Drop, Volts	Ratio of Voltages
200	90	0.000979	0.00167	0.208	0.215	1.032
300	90	0.001219	0.00158	0.300	0.295	0.985
400	90	0.001432	0.00148	0.359	0.370	1.030
500	90	0.001612	0.00143	0.428	0.435	1.015
600	90	0.001776	0.00138	0.495	0.502	1.015
700	90	0.001942	0.00132	0.551	0.580	1.050
800	90	0.002095	0.00128	0.609	0.630	1.035

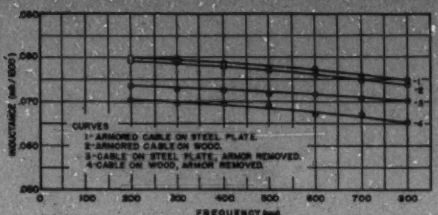


Figure 12. The effect of steel armor and other adjacent magnetic material on the effective inductance of a cable

Conclusions

An increase in the effective resistance of a 3-conductor cable is caused by the proximity of the conductors of the cable. This proximity effect increases as the size of conductor and the frequency of current increase and as the conductor spacing decreases. The proximity effect also causes the effective inductance to be decreased under the same conditions.

A 6-conductor cable when operated with its diametrically opposite conductor in parallel in a 3-phase circuit, shows lower effective resistance and inductance than larger 3-conductor cables.

The steel armor on a cable and adjacent magnetic materials cause the effective inductance and resistance of a cable to be increased.

Because of the many factors which govern the effective resistance and inductance of a 3-conductor cable, experimental measurement seems to be the only satisfactory means of obtaining these characteristics.

Appendix. Derivation of Equations for Use in Calculating Effective Resistance and Inductance of a 3-Conductor Cable

The circuit used in the measurement of the effective resistance and inductance of a 3-conductor cable is shown in Figure 1. The essential portion of this diagram is shown also in Figure 13 to illustrate the derivation of the equations used in the calculation of the effective resistance and inductance of a 3-conductor cable. In this circuit, balanced 3-phase currents flow in the three conductors of the cable. Measurements are made on each of the conductors separately. The current (I_a) in conductor A produces a voltage drop (V_{an}) between the point a and the neutral n. This voltage consists of a resistive component (I_aR_a) in phase with the current and a reactive component (I_aX_a) in quadrature with the current.

The current (I_s) in the secondary of the current transformer flows through the non-inductive resistance network consisting of the resistors R, R_1 and R_2, producing a resistive voltage drop (V_{bd}) between the points b and c. This secondary current flowing in the primary of the standard mutual inductometer induces a quadrature voltage (V_{cd}) in its secondary.

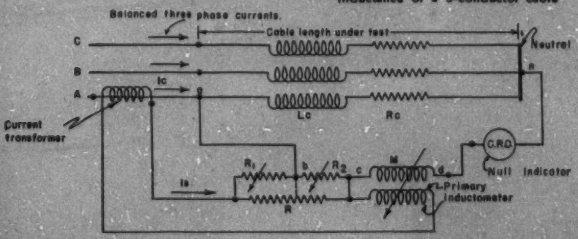
The magnitudes of V_{bd} and V_{cd} are independently adjustable by means of the variables R_1, R_2, and the mutual inductance M. The magnitude and phase of the total voltage V_{bd}, therefore, are adjustable and, with the proper connection of current transformer and inductometer, V_{bd} can be made equal to and in phase opposition with V_{an}. This condition will result in zero voltage at the terminals of the cathode-ray oscilloscope which is used as a null indicator. The vector diagram for these voltages and currents at this condition of balance is shown in Figure 14.

Because of the phase angle error, the secondary current in the current transformer is displaced by an angle from the primary current.

In Figure 14 the following relations may be seen:

$$V_{bd} = V_{an} \tag{1}$$

Figure 13. Basic circuit diagram for the measurement of the effective resistance and inductance of a 3-conductor cable



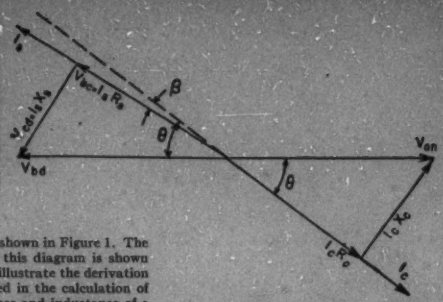


Figure 14. Phase relations of currents and voltages in circuit for measurement of effective resistance and inductance of a 3-conductor cable

$$I_aR_a = V_{an}\cos\theta \tag{2}$$

$$I_sR_s = V_b = V_{bd}\cos(\theta-\beta) = V_{an}\cos(\theta-\beta) \tag{3}$$

where R_s is equivalent resistance between points b and c of the resistance network; and

$$I_aX_a = V_{an}\sin\theta \tag{4}$$

$$I_sX_s = V_{cd} = V_{bd}\sin(\theta-\beta) = V_{an}\sin(\theta-\beta) \tag{5}$$

where

$X_s = 2\pi fM$ and M = mutual inductance of the inductometer.

Dividing equation 3 by equation 2 gives

$$\frac{I_sR_s}{I_aR_a} = \frac{V_{bd}\cos(\theta-\beta)}{V_{an}\cos\theta} = \frac{\cos(\theta-\beta)}{\cos\theta} = \frac{\cos\theta\cos\beta + \sin\theta\sin\beta}{\cos\theta}$$

Substituting

$$\frac{I_s}{I_a} = \frac{1}{r}$$

where r true ratio of current transformer, gives

$$\frac{R_s}{rR_a} = \frac{\cos\theta\cos\beta + \sin\theta\sin\beta}{\cos\theta}$$

Since β, the phase angle error of the current transformer, is less than 10 minutes, $\cos\beta$ is almost equal to unity. Then

$$\frac{R_s}{rR_a} = 1 + \tan\theta\sin\beta \tag{6}$$

Dividing equation 5 by equation 4 and substituting $\frac{I_s}{I_a} = \frac{1}{r}$, gives

$$\frac{X_s}{rX_a} = \frac{\sin(\theta-\beta)}{\sin\theta} = \frac{\sin\theta\cos\beta - \cos\theta\sin\beta}{\sin\theta}$$

$$\frac{X_s}{rX_a} = \cos\beta - \frac{\sin\beta}{\tan\theta} = 1 - \frac{\sin\beta}{\tan\theta} \tag{7}$$

Substituting

$\frac{X_s}{R_s} = \tan\theta$ in equation 6 and 7 gives

$$\frac{R_s}{rR_c} = 1 + \frac{X_s}{R_s}\sin\beta \tag{6a}$$

$$\frac{X_s}{rX_c} = 1 - \frac{R_s}{X_s}\sin\beta \tag{7a}$$

When equations 6a and 7a are solved simultaneously for R_c and X_c, the result is

$$R_c = \frac{\frac{R_s}{r} - \frac{X_s \sin}{r}}{(1 + \sin^2\beta)} \tag{8}$$

$$X_c = \frac{\frac{X_s}{r} + \frac{R_s \sin\beta}{r}}{(1 + \sin^2\beta)} \tag{9}$$

Since β is less than 10 minutes, $\sin^2\beta$ is negligible, and equations 8 and 9 reduce to

$$R_c = \frac{R_s - X_s \sin\beta}{r} \text{ and } X_c = \frac{X_s + R_s \sin\beta}{r}$$

A further approximation may be made with error of less than 0.1 per cent by dropping the term containing $\sin\beta$. Then

$$R_c = \frac{R_s}{r} \tag{8a}$$

and

$$X = \frac{X_s}{r} \tag{9a}$$

The equivalent resistance R_s between points b and c on the resistance network is

$$R_s = \frac{R_2 R}{R_1 + R_2 + R}$$

and since R is negligibly small compared to $R_1 + R_2$

$$R_s = \frac{R_2 R}{R_1 + R_2} \tag{10}$$

Substituting this in equation 8a gives

$$R_c = \frac{R_2 R}{r(R_1 + R_2)}$$

Since R, R_1, R_2 and r are known, R_c may be calculated readily.

$$X_c = 2\pi f L_c \quad \text{and} \quad X_s = 2\pi f M$$

Substituting these in equation 9a gives

$$2\pi f L_c = \frac{2\pi f M}{r} \quad \text{or} \quad L_c = \frac{M}{r}$$

Since M and r are known, L_c may be calculated readily.

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No Discussion

Steady-State Analysis of Aircraft D-C Generators

ERNEST VAN VALKENBURG
NONMEMBER AIEE

WHITNEY MATTHEWS
MEMBER AIEE

Synopsis: A practical method is developed for obtaining analytical expressions to represent the steady-state electromagnetic characteristics of a d-c generator. The terminal voltage of a separately excited d-c generator can be computed as a function of steady-state conditions of excitation, load, and speed when certain physical and empirical constants of the machine are known. The analysis leads to equations which can be applied to the condition of self-excitation. Applications of this mathematical tool to several aircraft generators indicate an accuracy approximating that of physical measurement when hysteresis effects are neglected.

IT IS often desirable and sometimes necessary to apply a mathematical analysis to the solution of certain problems involving a d-c generator and its associated control system. Specifically, the solutions to many d-c dynamo problems would be less complex providing there were a simple, yet accurate, means for expressing mathematically the machine's electromagnetic characteristics. This mathematical investigation originated with the necessity of analyzing the factors affecting the voltage regulation of aircraft d-c generators. It was believed that this problem could be approached with greater certainty, if trial and error processes could be abandoned for a direct mathematical investigation. Such a mathematical tool may prove applicable to certain aspects of dynamo design, problems of voltage, load, or speed control and theoretical study of magnetic circuits. The purpose of this paper, therefore, is to present the development of this mathematical analysis and associate it with established theories of d-c machines.

A satisfactory analytical representation for the steady-state characteristics of aircraft d-c generators has been developed. The basic theories involved are sufficiently general so that application to other types of generators can be made easily. Once an analysis has been established for the steady-state conditions, it seems possible that generator build-up, optimum voltage and load for maximum efficiency or minimum weight, optimum brush positions, and so forth, could be investigated more thoroughly than at present. Extension of the theory to treat transients has not been attempted, and it is questionable whether such an attempt would prove of value.

Sometimes an exact mathematical representation of a nonlinear physical system becomes so involved and tedious that such an approach is impractical. An approximate analysis using empirical equations, however, often results in a relatively simple solution having a practical degree of accuracy. This is true for d-c generators. All the steady-state characteristics, which involve the magnetic circuit, are nonlinear because of the magnetic saturation property of ferromagnetic materials. Thus, it is expedient to represent the magnetization curve for any given machine by an empirical equation. Many attempts have been made to find an algebraic or transcendental function that would express this magnetization characteristic satisfactorily, but most of these equations either produce large errors over portions of the curve, or become too complex to be practical. Probably the most used function is that obtained by Froelich;[1] yet this equation is reasonably accurate over only a limited region. Ryder[2] has conceived an expression involving the Gudermannian function. This equation determines a curve with satisfactory accuracy, but an exact integral of the function has not been found. This fact impairs its usefulness in certain applications. In searching for a more useful relationship, a hyperbolic-tangent function has been derived which adequately represents the magnetization characteristics of aircraft generators, and which is also mathematically flexible.

Generator Terminal Voltage

The steady-state terminal voltage of a d-c generator may be represented by

$$E_t = K\Phi n - E_a \tag{1}$$

where Φ is the flux, and n is the speed in rpm. $K\Phi n$ is the generated voltage, and E_a is the combined IR drop and contact potentials in the armature circuit (including any series coils) as a result of load current. When E_t is expressed in volts, the constant becomes

$$K = \frac{ZP}{60 \times 10^8 a} \tag{2}$$

where Z is the number of armature conductors, P is the number of field poles, and a is the number of parallel armature current paths.

Nonlinearity is introduced when flux is expressed as a function of magnetomotive force, or ampere-turns of the field. A typical magnetization characteristic for an aircraft generator is shown in Figure 1. The shape of these curves depends upon the history of the iron in the machine. To keep the theory as general as possible, it is assumed that the iron has been previously saturated creating a residual flux. The condition of no residual flux will then be a special case. (See Appendix II). The upper and lower curves of Figure 1 represent the limits of the minor hysteresis effects due to normal operation along the upper trace of a

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ERNEST VAN VALKENBURG and WHITNEY MATTHEWS are both with the Naval Research Laboratory, Washington, D. C.

complete hysteresis loop. To simplify the analysis, let the mean of these two curves represent the relationship of flux and excitation (ampere-turns). This is equivalent to neglecting the minor hysteresis effects. The maximum error introduced is about two per cent of the normal total flux for aircraft generators.

No-Load Saturation Characteristic

The empirical equation developed for expressing the no-load air-gap flux, ϕ, as a function of field excitation is

$$\phi = A \tanh (BN_fI_f + C) + D \qquad (3)$$

where N_fI_f is the separately excited shunt field ampere-turns per pair of poles. The empirical constants A, B, C, and D can be determined accurately for a generator by solving four specific simultaneous equations (see Appendix II). This function was chosen because of the similar shapes of the hyperbolic-tangent and magnetization curves, and the use of four constants provides satisfactory flexibility for curve fitting. Other advantages of this expression are that it is integrable, and field excitation can be expressed explicitly.

The no-load terminal voltage with the shunt field separately excited is then

$$E_t = Kn(A \tanh [BN_fI_f + C] + D) \qquad (4)$$

Figure 2 displays the exactness of equation 4 when applied to three aircraft generators. The maximum error in computed values from the measured characteristic is about one per cent of the normal output voltage for characteristics A and C, and less than two per cent for characteristic B. This error is less than the deviations due to hysteresis, which are being neglected.

Figure 3 indicates the result of a reasonable attempt to apply Froelich's equation to the characteristic B of Figure 2. Here the maximum error is about ten per cent. Furthermore, this error increases at greater excitations, which will become critical under load conditions.

Introducing Armature Current

The steady-state load characteristics can be expressed mathematically, provided an analysis of the effects of armature current can be incorporated in equation 4. Two such effects are known to occur in d-c generators. These are the IR drop and the magnetomotive force of armature reaction. The latter is divided into "direct" and "cross" axis components.

Direct-axis armature reaction may cause demagnetization or magnetization depending on the direction of the brush shift. The magnitude of this force is determined by the ampere-turns included in the double arc between the brush position and the geometric neutral axis.[1] The magnitude of this force per pair of field poles in ampere-turns is represented by N_dI_a, where I_a is the total armature current and

$$N_d = \frac{4\alpha Z}{360\, Pa} \qquad (5)$$

α is the forward brush soift angle expressed in electrical degrees. Since this force directly opposes the main field, the resultant field excitation is $N_fI_f - N_dI_a$. Likewise, a brush shift against rotation, which has been observed in some aircraft generators, makes the absolute value of N_dI_a negative and increases magnetization.

The demagnetizing component due to cross ampere-turns results from the unequal saturation of the leading and trailing portions of the main field pole shoes. Armature current creates flux paths through each field pole, two air-gaps, and the armature as shown by

Figure 1. Typical magnetization characteristic

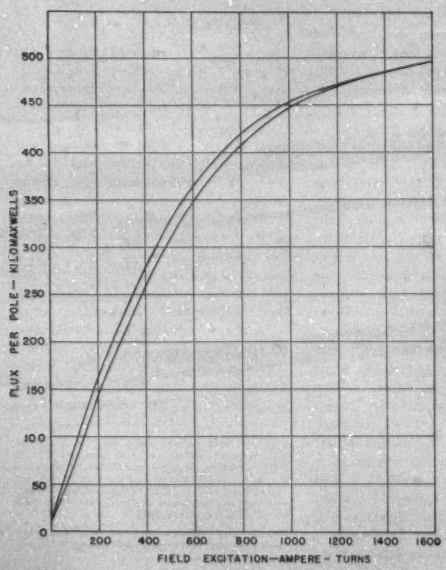
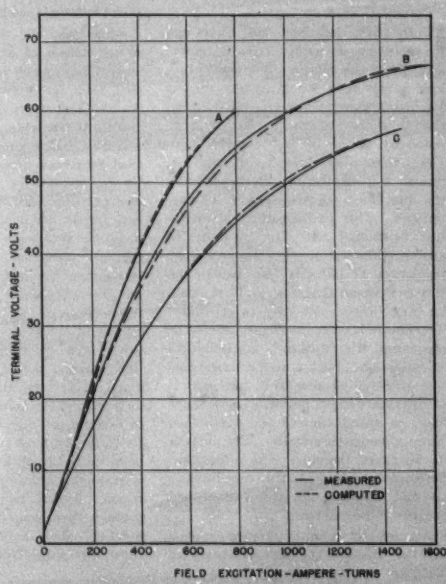


Figure 2. No-load saturation characteristics

Generator A—6 kw machine at 3,500 rpm
Generator B—2 kw machine at 3,500 rpm
Generator C—2 kw machine at 6,000 rpm



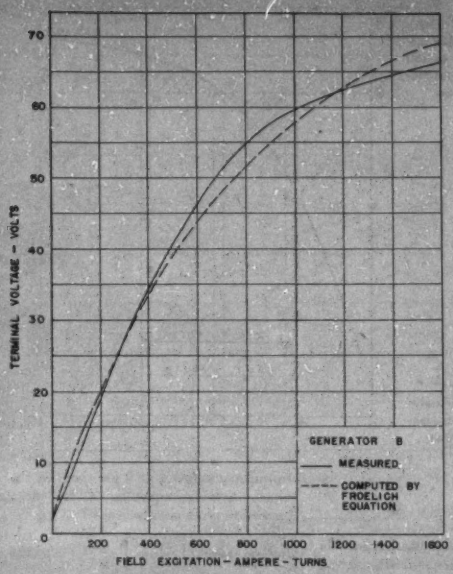


Figure 3. Application of Froelich's equation

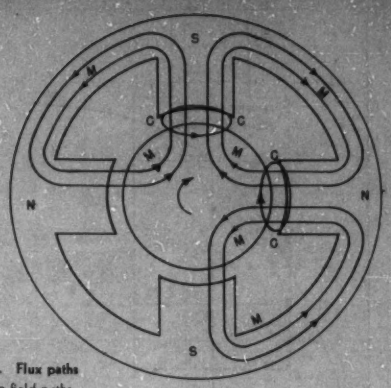


Figure 4. Flux paths
M—main field paths
C—cross field paths

path C, Figure 4. The reluctance of these paths is approximately the same as the main field paths. In practice, the error in assuming them equal is negligible; therefore, the flux distribution with respect to armature ampere-turns along the air-gap due to cross-magnetization may be represented by the no-load magnetization curve, shown in Figure 5. The effective demagnetization is then proportional to the difference of the areas A_1 and A_2.

That this demagnetizing component is equivalent to the necessary shift of the dimension $2N_cI_a$ $(N_fI_f - N_1I_1)$ to make area A'_1 equal A'_2, Figure 6, has been suggested by Langsdorf.[8] He demonstrates this equivalence mathematically by integrating Froelich's equation. Through a similar integration of equation 3, the following expression for total flux, Φ, under load conditions is obtained. This includes also the effect of the direct axis component of armature reaction (see Appendix III).

$$\Phi = \frac{A}{2BN_cI_a} \times \log_e \left[\frac{\cosh\,[B(N_fI_f - N_aI_a + N_cI_a) + C]}{\cosh\,[B(N_fI_f - N_aI_a - N_cI_a) + C]} \right] + D \tag{6}$$

where N_cI_a is the cross ampere-turns per pole. The reluctance of any cross flux paths extending beyond the boundaries of a pole shoe becomes very great. Therefore, only a slight error is introduced by considering just the armature conductors spanned by the pole shoes. Thus, for a pair of field poles

$$N_c = \frac{2\gamma Z}{360\,Pa} \tag{7}$$

where γ is the angle, measured in electrical degrees, of the arc subtended by a field-pole shoe. The generator terminal voltage is then

$$E_t = K\Phi n - E_a \tag{8}$$

Of the three effects, E_a is the most pronounced. E_a is the measured electrical potential difference resulting from armature current. This voltage is not only an IR drop, but includes contact potentials developed at the brush and commutator surfaces. For obvious reasons no attempt has been made to express the latter inconstant phenomenon mathematically; it is more practical to determine the magnitude of E_a from a series of linear characteristics which approximate the measured values as shown in Figure 7. The measured values are terminal voltages obtained when the armature is supplied with current from an external source and operated with all field poles removed.

Figure 8 indicates the results of applying equations 6 and 8 to an aircraft generator having no windings in series with the armature. The maximum error within the operating range (20 to 40 volts) is less than ±1 volt, or about ±3 per cent of the output voltage. If a generator is compounded, the additional magnetomotive force created by armature current must be added to the shunt field excitation.

Generators having either compensating coils, commutating poles, or both, do not complicate this analysis, providing these devices are symmetrical with the main field structure. The characteristics of a perfectly compensated separately excited machine would be represented by equation 4 minus E_a for any load condition. Commutating poles permit the brushes to be located on the no-load neutral axis without sparking under load conditions, thus eliminating direct-axis armature reaction. Practically, the compensating coils of a machine will not perfectly annul the demagnetization due to cross ampere-turns. Therefore, the magnitude of N_cI_a for a compensated generator becomes the absolute difference between the effective ampere-turns of the compensation and the armature ampere-turns contributing to cross-magnetization. The effective turns of the compensation is equal to half the number of compensating conductors per pair of poles, divided by the number of parallel compensating current paths. Figure 9 typifies the accuracy of applying equations 6 and 8 to a generator having compensating windings and commutating poles. The brushes of this machine have a backward shift which produces cumulative compounding at load conditions.

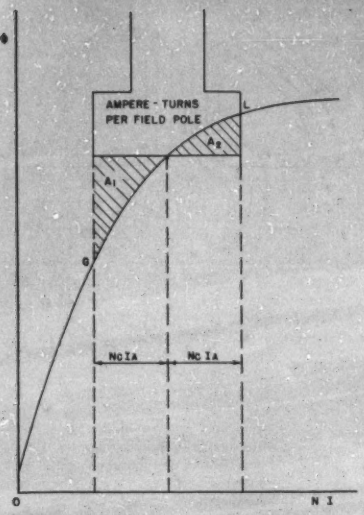


Figure 5 (left). Air-gap flux distribution caused by cross-magnetization

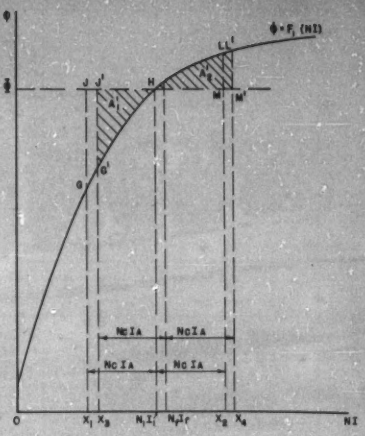


Figure 6. Analysis of cross-magnetization

The maximum error is ≐0.7 volt in the rated operating range.

Detailed Analysis of Equation 6

If equation 6 satisfies all conditions of cross-magnetization, then it must be equal to equation 3 when the armature current is zero. Also, according to the preceding paragraph, the effective N_c can theoretically vanish through perfect compensation of cross-magnetization. For either of these conditions, equation 6 becomes indeterminate. The limits, however, as I_a or N_c approach zero are as follows:

$$\lim_{I_a \to 0} \Phi = \lim_{I_a \to 0} \frac{\dfrac{d(A \log_e Y)}{dI_a}}{\dfrac{d(2BN_cI_a)}{dI_a}} + D$$

$$= A \tanh (BN_fI_f + C) + D = \phi \qquad (9)$$

where

$$Y = \frac{\cosh [B(N_fI_f - N_dI_a + N_cI_a) + C]}{\cosh [B(N_fI_f - N_dI_a - N_cI_a) + C]}$$

and, likewise,

$$\lim_{N_c \to 0} \Phi = A \tanh (B[N_fI_f - N_dI_a] + C) + D \qquad (10)$$

Thus, these conditions are satisfied.

It should be noted that for large magnitudes of N_cI_a (maximum loads), equation 6 is not rigorously valid. Whenever N_fI_f is less than N_cI_a, the line $2N_cI$ projects into the second quadrant, Figure 10, which indicates that the portions of the magnetization curve in the second and third quadrants must be considered. The function chosen to represent the curve is not absolutely symmetrical to the vertical axis. Equation 3 can be altered to apply to the second and third quadrants as follows:

$$\phi' = A \tanh (BN_fI_f - C) + D + 2A \tanh C \qquad (11)$$

Fortunately, for most applications, equation 3 can be assumed equal to equation 11 without appreciable error (see Appendix III). Under such a condition, equation 6 is acceptable for all loads.

Experience indicates that this assumption of symmetry is permissible for all magnitudes of $2N_cI_a$ less than the excitation required for rated load. The magnitude of $2N_cI_a$ at rated load for generator C, whose characteristics are exhibited in Figure 8, is 825 ampere-turns, yet the error from applying equation 6 is negligible within the operating region. If this error is not negligible, both equations 3 and 11 must be employed to determine the effect of cross-magnetization (see Appendix III).

Inverse Generator Characteristics

For certain applications, it may be desirable to solve equations 4 and 8 explicitly for such variables as field excitation, brush shift, or speed. Such characteristics are derivable. The solution of equations 4 or 8 for speed is obvious. Solving equation 4 for field excitation gives simply

$$N_fI_f = \frac{1}{B} \tanh^{-1} \left[\frac{E_t - D}{AKn} \right] - \frac{C}{B} \qquad (12)$$

or, solving equation 8 for field excitation (Appendix IV) gives

$$N_fI_f = \frac{1}{2B} \log_e \times \left[\frac{\exp [2BN_cI_a(E_t - DKn + AKn + E_a)/(AKn)] - 1}{\exp [2BN_cI_a] - \exp [2BN_cI_a(E_t - DKn + E_a)/(AKn)]} \right] - \frac{C}{B} + N_dI_a \qquad (13)$$

This equation is readily solvable for N_d, which is a function of brush shift. A unique solution of equation 8 for armature current does not exist since E_a, which is expressed algebraically is also a function of armature current The measured and calculated characteristics of field excitation versus load current are shown in Figure 11.

Equation 13 is of particular interest, because it can be applied to a self-excited generator. So far, the analysis has been confined to conditions of separate excitation in order to determine terminal voltage when field current is the independent variable. For the condition of self-excitation, field current becomes interdependent on terminal voltage. That is, for a generator whose shunt field is excited by the terminal voltage,

$$I_f = E_t / R_f \qquad (14)$$

where R_f is the total field circuit resist-

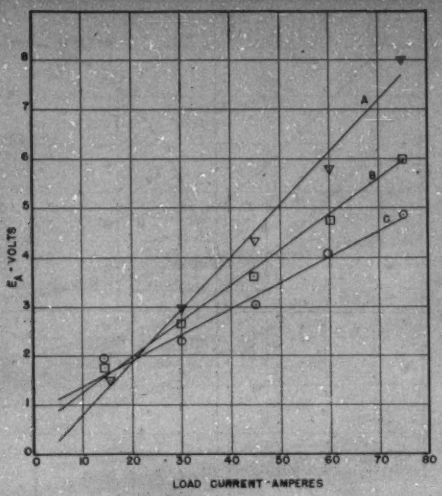


Figure 7. Determination of E_a on generator C

A—8,000 rpm
B—6,000 rpm
C—4,000 rpm

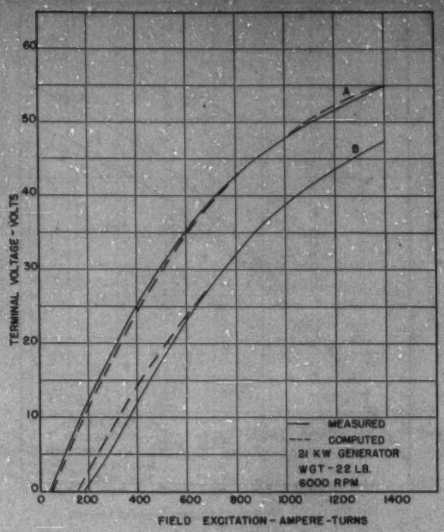


Figure 8. Load characteristics of generator C

A—$I_a = 30$ amperes
B—$I_a = 75$ amperes

ance. Combining equations 13 and 14 gives

$$R_f = \frac{2BE_tN_f}{\log_e\left[\frac{\exp[2BN_dI_a(E_t - DKn + AKn + E_a)/(KnA)] - 1}{\exp[2BN_dI_a(E_t - DKn + E_a)/(KnA)]}\right] - 2C + 2BN_dI_a} \quad (15)$$

Load current is now different from armature current, and equation 15 is applicable at no-load since I_a is not then zero. Thus, by means of equation 15, the field resistance which will satisfy any particular set of steady-state conditions when a generator is self-excited can be computed. Figure 12 shows the results of this application on two aircraft generators. The maximum error has accumulated to ±7 per cent which is the approximate horizontal error of minor hysteresis loops.

Conclusion

It has been demonstrated that the steady-state characteristics of a d-c generator can be represented mathematically to a practical degree of accuracy by using hyperbolic functions. Application of this analysis to a given machine requires the following specific data:

1. The magnetization characteristic or a no-load saturation characteristic at a known speed.
2. The number of field and compensating turns per pair of poles.
3. The number of main field poles.
4. The total number of conductors on the armature.
5. The angle of brush shift and its direction.
6. The angle of the arc subtended by a field pole shoe.
7. The number of parallel current paths in the armature.
8. The number of parallel current paths through any series windings.
9. The armature voltage drop at various loads and speeds.

From these data, the terminal voltage of a separately excited aircraft generator has been computed for any rated conditions of speed, load, and excitation with an accuracy of ±3 per cent. Furthermore, this analysis leads to certain applications involving self-excitation with a maximum error of about seven per cent. The limiting accuracy of this analysis is determined largely by effects due to minor hysteresis loops and erratic contact potentials.

In applications which require only an approximate analysis, equation 3 can be simplified to a pure hyperbolic tangent function. That is, $\phi = A \tanh B(N_fI_f)$. Using this equation, results have been obtained to an accuracy within five per cent error for no-load characteristics.

Only the most simple aircraft generators have been examined. The equations derived are not sufficiently general for application to all generators. For example, only symmetrical field poles have been studied. Asymmetrically shaped pole shoes have a direct effect on the magnitude of cross-magnetization which cannot be directly determined by methods presented here. The analysis can be applied also to machines with series fields. Certain apparent alterations, however, may be necessary to distinguish between armature and load currents. Therefore, successful application of this mathematical analysis is dependent on an investigation of the physical design of the particular type of generator involved.

Appendix I. Symbols

The symbols used in this analysis have been explained as they appeared in the text, but for the convenience of the reader, the following summary is presented.

A, B, C, and D = empirical constants
a = number of parallel armature current paths

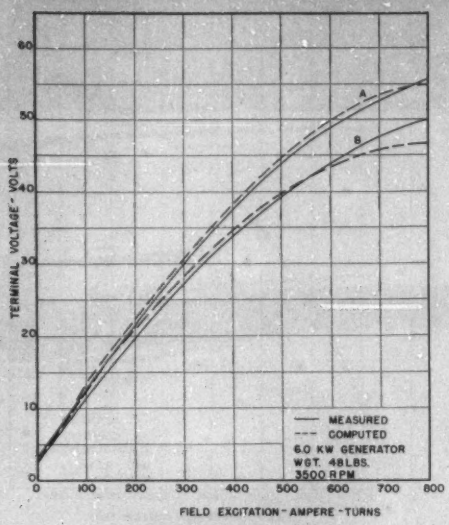


Figure 9. Load characteristics of compensated generator A

A—I_a=100 amperes
B—I_a=200 amperes

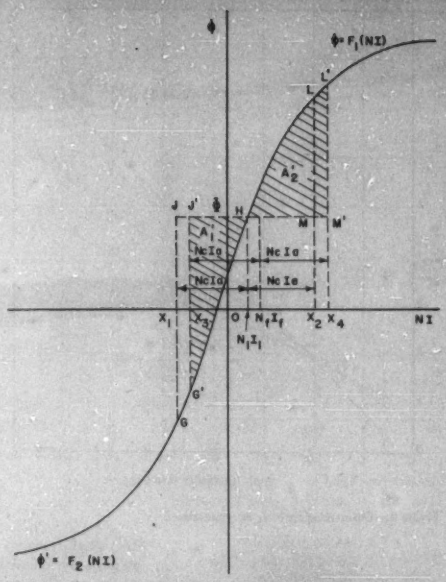


Figure 10. Analysis of cross-magnetization at small excitations

α = angle of brush lead from the no-load neutral axis
γ = angle of arc subtended by a field pole shoe
E_a = total armature voltage drop due to load current
E_t = terminal voltage
I_a = armature current
I_f = shunt-field current
K = a constant of the generator
N_c = number of effective turns contributing to cross-magnetization
N_d = number of effective turns contributing to direct axis armature reaction
n = speed in rpm
P = number of field poles
R_f = total shunt field resistance
Φ = total air-gap flux at load
ϕ = total air-gap flux at no-load
ϕ' = total air-gap flux at no-load with negative excitation
Z = total number of armature conductors

Appendix II. Determination of Empirical Constants

The empirical constants can be determined by investigating the characteristics of the hyperbolic-tangent function. For the function $Y = \tanh X$, $Y = 0$ when $X = 0$. Therefore, when $N_f I_f = 0$, equation 3, $\phi = A \tanh (BN_f I_f + C) + D$, becomes

$$\phi_r = A \tanh (C) + D \tag{16}$$

where ϕ_r equals the residual flux. For the special case where the curve passes through the origin, ϕ_r is zero, but equation 16 and those that follow are still valid for determining the constants. Likewise, $Y = 1.0000$ for any value of X greater than 5.00. The slope of Y versus X approaches zero so rapidly that even tanh 3.0 = 0.995. For convenience, let tanh 3.0 = 1.0. Then

$$\phi_{max} = A \tanh (3.0) + D = A + D \tag{17}$$

when

$$BN_f I_{max} + C = 3.0 \tag{18}$$

That is, when the field pole becomes saturated, the slope of the magnetization curve can be considered practically zero. This maximum excitation is usually about twice the excitation required for rated load. The point ($N_f I_{max}$, ϕ_{max}) can be predicted sufficiently by extrapolation, if necessary. For an intermediate value of rated excitation, $N_f I_1$, the flux is

$$\phi_1 = A \tanh (BN_f I_1 + C) + D \tag{19}$$

Solving equation 17 for D, and substituting in equation 19 gives

$$\phi_1 = A \tanh (BN_f I_1 + C) + \phi_{max} - A \tag{20}$$

From equations 16 and 17

$$\frac{A = \phi_{max} - \phi_r}{1 - \tanh [C]} \tag{21}$$

Combining equations 18, 20, and 21 yields

$$1 + \frac{\phi_{max} - \phi_1}{\phi_{max} - \phi_r}(\tanh [C] - 1) = \tanh \left[\frac{C(N_f I_{max} - N_f I_1) + 3N_f I_1}{N_f I_{max}} \right] \tag{22}$$

Equation 22 can be solved graphically for the constant, C. There are usually two roots other than 3.0. If no other root than 3.0 exists, point ($N_f I_1$, ϕ_1) should be chosen higher on the curve. Knowing C, substitution in equations 21, 18, and 17 determines A, B, and D, respectively. A trial curve can then be plotted. If this calculated curve does not fit the experimental measurements within about one or two per cent error, it indicates that the point ($N_f I_{max}$, ϕ_{max}) was not determined accurately. An error in any portion of the curve can often be reduced by a slight alteration of one or two of the constants by inspection.

Hyperbolic function tables prepared by the National Bureau of Standards have been found very useful for radian arguments significant to three or more figures.[8,9]

Appendix III. Derivation of Cross-Magnetizing Effect

Demagnetization results from cross-magnetization when the line "GL," Figure 5, is not a straight line. That is, there would be no demagnetizing effect when area "GHJ" equals area "HML," Figure 6. This condition can be satisfied for any ordinate, ϕ, on the curve by shifting the dimension $2_c NI_a$ to the right. Now area G'HJ' = area HM'L'. Thus, for a no-load excitation of $N_f I_f$, and a given armature current, the resultant flux is ϕ. ϕ is the mean value of the ordinates between the limits $NI = X_1$ and $NI = X_4$, or

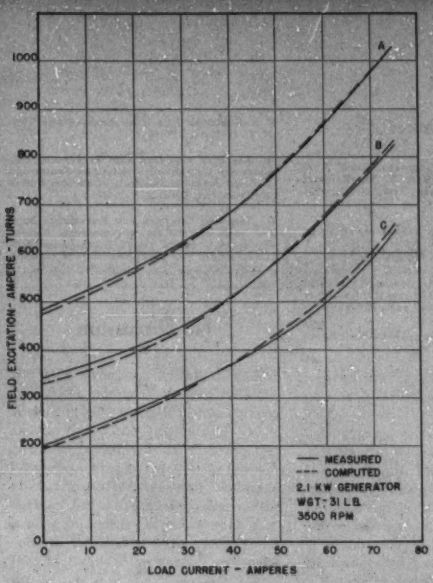


Figure 11. Inverse characteristics of generator B

A—$E_t = 40$ volts
B—$E_t = 30$ volts
C—$E_t = 20$ volts

$$\phi = \frac{1}{X_4 - X_3} \int_{X_3}^{X_4} \phi d(NI) \quad (23)$$

where

$$\phi = A \tanh (BNI + C) + D \quad (24)$$

Integrating equation 24 gives

$$\int \phi d(NI) = A/B \log_e [\cosh (BNI + C)] + DNI + K' \quad (25)$$

where K' is the constant of integration. Then

$$\Phi = \frac{1}{X_4 - X_3} \times \left[A/B \log_e [\cosh (BNI + C)] + DNI \right]_{X_3}^{X_4} \quad (26)$$

but, $X_4 = N_f I_f + N_c I_a$ and $X_3 = N_f I_f - N_d I_a$. So,

$$\Phi = \frac{A}{2N_c I_a B} \times \log_e \left[\frac{\cosh [B(N_f I_f + N_c I_a) + C]}{\cosh [B(N_f I_f - N_d I_a) + C]} \right] + D \quad (27)$$

Investigation When $N_f I_a$ Is Large

The complete magnetization curve of Figure 10 can be expressed as two functions, one function being valid for positive values of excitation and the other for negative values. These functions are, respectively:

$$\phi = A \tanh (BN_f I_f + C) + D \quad (28)$$

$$\phi' = A \tanh (BN_f I_f - C) + D + 2A \tanh C \quad (29)$$

Recalling that

$$\tanh (BN_f I_f \pm C) = \frac{\tanh (BN_f I_f) \pm \tanh (C)}{1 \pm \tanh (BN_f I_f) \tanh (C)} \quad (30)$$

it is apparent that if both $BN_f I_f$ and C are much less than unity, then

$$\tanh (BN_f I_f \pm C) \approx \tanh (BN_f I_f) \pm \tanh (C) \quad (31)$$

Applying this to equations 28 and 29 yields

$$\phi \approx \phi' \approx A \tanh BN_f I_f + A \tanh C + D \quad (32)$$

If equation 32 is not sufficiently accurate in a particular application, the effect of cross-magnetization can be derived by considering positive and negative magnetomotive forces separately. That is, the areas A'_1 and A'_2 (Figure 10) can be represented by two integrals having the proper limits. In which case,

$$\Phi'(2N_c I_a) = \int_0^{N_f I_f - N_d I_a} \phi' d(NI) + \int_0^{N_f I_f + N_c I_a} \phi d(NI) \quad (33)$$

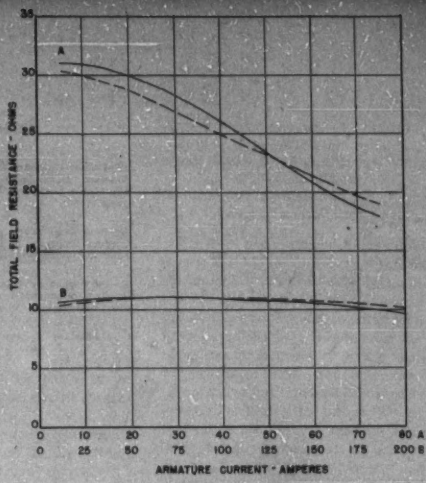


Figure 12. Self-excitation characteristic

A—generator C, $E_t = 28$ volts
B—generator A, $E_t = 28$ volts

$$\Phi' = \frac{A}{2BN_c I_a} \times \log_e \left[\frac{\cosh [B(N_f I_f + N_c I_a) + C]}{\cosh [B(N_f I_f - N_d I_a) - C]} \right] - \frac{A(N_f I_f) \tanh C}{N_c I_a} + A \tanh C + D \quad (34)$$

Here, however, $N_f I_f$ is not uniquely confined to the logarithm as in equation 27; therefore, equation 34 is very inflexible, and its utility is limited.

Appendix IV. Solution of Equation 8 For $N_f I_f$

Equation 8 can be expanded as

$$E_t = Kn \left(\frac{A}{2BN_c I_a} \times \log_e \left[\frac{\cosh [B(N_f I_f - N_d I_a + N_c I_a) + C]}{\cosh [B(N_f I_f - N_d I_a - N_c I_a) + C]} \right] + D \right) - E_a \quad (35)$$

or, expressed exponentially,

$$\exp [B(N_f I_f - N_d I_a + N_c I_a) + C] + \exp [B(N_f I_f - N_d I_a - N_c I_a) + C] + \exp [-B(N_f I_f - N_d I_a + N_c I_a) - C] = \exp [-B(N_f I_f - N_d I_a - N_c I_a) - C] = \exp [2BN_c I_a(EI + E_a - DKn)/(AKn)] \quad (36)$$

Multiplying both sides of equation 36 by the first term in the numerator and clearing of fractions yields

$$\exp\ [2B(N_f I_f - N_d I_d)+2C]\ \exp\ [2BN_d I_a] + 1 = \exp\ [2B(N_f I_f - N_d I_d)+2C] \times \exp\ [2BN_d I_a(E_t+E_a-DK\pi)/(AK\pi)] + \exp\ [2BN_d I_a(E_t+E_a-DK\pi)/(AK\pi)+2BN_d I_a] \quad (37)$$

Factoring and rearranging gives

$$N_f I_f = \frac{1}{2B}\log_e\left(\frac{\exp\ [2BN_d I_a(E_t+E_a-DK\pi+AK\pi)/(AK\pi)]-\exp\ [2BN_d I_a]-1}{[2BN_d I_a(E_t+E_a-DK\pi)/(AK\pi)]}\right)-\frac{C}{B}+N_d I_a \quad (38)$$

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No Discussion

Industrial Electric Resistance Heating

LEE P. HYNES
FELLOW AIEE

IN THE tremendous growth of electric heat for industrial use there have developed several methods of transforming electric power into heat energy. While the basic principle is the same, in that heat is generated by resisting the free flow of electric current, the term "resistance heating" has been applied to describe the method of passing current through a resistance material and transferring this heat by means of conduction and radiation.

Resistance heating is the oldest and most widely used type, but there are many situations where other methods are superior, and the mature engineer will determine accurately the advantages and disadvantages of each and avoid the assumption that a method which has worked for one job is necessarily the logical one for another job, even though they seem at first glance to be similar.

Among the advantages of resistance heating are its inherent efficiency of almost 100 per cent in converting electric power into heat energy, high thermal efficiency in transferring this heat to the work, adaptability to the standard commercial power circuits without expensive transformers, moderate investment cost, and reliable service.

Its principal disadvantages lie in its limitation to temperatures well below those required for some purposes, its limitation in density of heat input where great concentration of heat energy is required at any temperature, and its inability to heat uniformly some materials, such as dielectrics, except by slow conduction through their mass.

Another disadvantage which is also shared by all other methods is the frequent attempt to apply electric heat to a specific job without enough background of experience and without an accurate analysis of all the intricate engineering problems which are involved. The most innocent-looking job has plenty of pitfalls in the industrial electric heating field.

Perhaps we can also say that one disadvantage of resistance heating is that it looks so simple. For many purposes, it is comparatively simple, and the wide use of electric-strip, cartridge, and immersion heaters testifies to their durability in spite of considerable abuse of them by poor application design on the part of the user.

Resistance heating elements are made of a size, shape, and construction to suit the use for which they are intended. By far the greatest number have heat-producing resistors of nickel-chrome alloy wire or ribbon. Electric current flowing through this high-resistance metal creates heat.

Refractory insulation is used to electrically insulate and support the heated resistor. This insulation must have ample dielectric strength at the maximum operating temperature.

Some heating elements have resistors embedded in compressed refractory powder, with a metal sheath enclosure, carrying suitable insulated terminals for the power supply. Enclosed elements of this kind depend to a large extent on conduction for delivering their heat.

Heavy-duty heating elements often have resistors of wire or ribbon supported by performed refractory insulators flexibly mounted to withstand shock and permit free expansion and contraction. Open elements of this kind permit heavy loading and can operate at high temperatures as they deliver most of their heat by radiation. They are well suited for grouping into large assemblies with substantial bus connections.

This construction permits clearances to ground ample for the higher commercial voltages even at elevated temperatures. When inserted into tubes, they form pressure-type units of large heating area.

The more important uses of resistance electric heating are in industrial process equipment where careful engineering design and sound experience have produced highly efficient and reliable results. Some typical examples are: heat-treating furnaces; hot-air recirculating dryers; electric boilers for steam, hot water, dowtherm, and so forth; hot oil heat transfer systems; preheaters for fuel oil; ovens; electric-unit air heaters; electric catalysts; steam superheaters; ice melters; air duct heaters; radiant heaters; and tanks or kettles for pitch, oil, wax, and chemicals.

The correct design of industrial process equipment requires careful analysis of each individual job. The complex problems to be solved include heat transfer, rate of heating, economics, time factors, critical temperatures for materials, corrosion, safety from hazards, code requirements, effective insulation, proper design of automatic controls, and practical labor-saving methods of handling materials in process. These challenge the best efforts of the electrical, mechanical, and chemical engineer and require close co-operation with plant production management. Only through a careful and complete study of each job can really satisfactory performance be assured.

Automatic controls are vital for the proper and economical operation of electric heaters. It is a fallacy to think that close temperature regulation can always be obtained by simply using a sensitive temperature control. In many cases, the rate of heat input must be varied to suit variation in heat demand. If this is not provided for, overshooting and undershooting of temperature will surely result, especially when a variable flow of material of low specific heat is involved as in air and gas heaters, steam superheaters, and conveyor units operating under

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Lee P. Hynes is a consulting engineer, Hynes Electric Heating Process Division, Martin Quaid Company, Philadelphia, Pa.

variable speed or with great changes in load of work per unit of time.

Many engineers are so used to thinking in terms of steam that they fail to realize the basic differences between steam and electric heating. With steam, there is an initial accelerated input of heat when the work is cold due to the high rate of steam condensation. As the temperature rises, this condensation rate drops and heat transfer falls in proportion because the heat gradient drops.

With electric heat, the energy input is practically constant and temperatures rise at a nearly uniform rate because the heat gradient remains substantially constant. It must be remembered that the temperature of an electric heater rises as the ambient temperature surrounding it rises. The heat density rating of an electric heater must be designed for its maximum temperature when operating at the maximum ambient temperature.

A common error in large air-heating units is to assume that a mixing damper can be used to control temperature by means of by-passing the electric heater, as is standard practice for steam heat. Actually no change in electric energy input will result from by-passing the heater as reduced airflow over the heater will result in a corresponding rise of temperature in the air that does pass over it.

Wherever any change of energy input from an electric heater is required, some change of the power input must be made by suitable control means. This may be accomplished in several ways such as change in impressed voltage, change in the number of heater sections in operation or by time cycling.

Many factors must be studied in designing a resistance heating job. Among these are the required maximum kilowatts per hour, the maximum permissible temperature for the heated surfaces in contact with the work being heated, the total area of the heated surface needed to allow the required energy input without exceeding the permissible maximum temperature, the watt density necessary to deliver the required energy input, and the maximum safe temperatures of the heating elements.

The temperature of a heating element depends on the watt density per unit of surface, the maximum heat gradient between the element and the work, and the maximum ambient temperature of the work. It must always be remembered that the operating temperature of an electric heater is not a fixed value but varies with ambient temperature changes.

The safe temperature for a heating element depends on its materials, construction and general design. Nickel-chromium exposed resistors can operate at temperatures up to about 2,100 degrees Fahrenheit under favorable conditions, but lower temperatures mean longer life. Carbon and graphite resistors can be operated at more than 3,000 degrees Fahrenheit when surrounded by an inert atmosphere to prevent their gradual oxidation.

When nickel-chromium resistors are embedded in a dense compressed refractory and enclosed by a metallic sheath this sheath becomes the heat dissipating surface. The manufacturers usually specify the safe maximum sheath temperature, which depends on materials, resistor design, and the gradient introduced by the refractory between the resistor and the sheath. For special alloy sheaths a maximum temperature of 1,500 degrees Fahrenheit is usually specified and 750 degrees Fahrenheit for plain steel sheaths.

Different types of heaters have quite different maximum safe-temperature limits but for many processes this maximum heater temperature can not be used because of work temperature limitations.

The selection of a proper watt density for a given heating job is one of the most difficult problems in electric heat engineering. There is no fixed ratio between the surface temperature of an electric heater and its watt density except under exactly identical operating conditions.

Each material being heated has its own critical temperature, and its own maximum rate for satisfactory heat absorption. These factors govern the selection of the permissible surface temperature in actual contact with the work. After determining the safe surface temperature for a given process the proper watt density per unit of heating surface can be computed.

The permissible watt density depends on the capacity of the work material to absorb heat. It also depends on the critical temperature of the material being heated. For instance, molasses can not be heated with as high watt density as water and even water varies according to its content of lime or other substances.

Values of watt densities frequently used in practice are given in Table I, with a distinct warning that the figures are valid only when used in a heat application having a correct over-all design. As isolated values they are worthless.

There are countless other heater applications too special to be included in even so tentative a tabulation as Table I.

The problems of automatic control for electric heat are very important but are too complex for adequate treatment here. Industrial heaters require control from two angles, first, safety for the equipment and, second, regulation of heat input to the needs of the process. For safety, the maximum temperature must be limited by automatic cutouts located at key places.

For heat input control, the proper type depends on the process and its operating conditions. Where large thermal storage capacity exists, and the rate of temperature change is gradual, as in fluid tanks, a simple on-and-off control may be sufficient. This can be supplemented by manual heat steps to take care of great variations in demand, as, for instance, in summer and winter service or in standby and operating conditions.

In other cases, close temperature regulation is necessary with rapid variations in heat demand. This calls for a more elaborate type of control, and close coordination between heater design and control selection is essential.

Where the over-all heater design permits cutting out sections of the heaters without interfering with proper heat distribution or unbalance of phase loads, the use of step controllers is satisfactory for many installations where a limited number of fixed steps of heat input are adequate.

Table I. Typical Watt Densities of Electric Heaters

Type of Service	Watts Per Square Inch of Surface	Remarks
Furnaces: nickel-chromium exposed resistors	10–20	Depends on operating temperature
Furnaces: carbon or graphite resistors in an inert atmosphere	80–120	Depends on operating temperature
Radiation heaters: nickel-chrome exposed resistors	25–40	Depends on operating temperature
High-velocity industrial air heaters	15–30	Depends on air velocity and temperature
Fan circulation unit air heaters and duct heaters	15–20	Depends on air velocity and temperature
Water heaters	20–45	Depends on water analysis and temperature
Steam boilers	10–20	Depends on water analysis and temperature
Dowtherm boilers	7–12	Depends on liquid temperature
Circulating oils, 100–200 degrees Fahrenheit	9–12	Depends on type of oil, velocity, temperature, and flow design
Circulating oils, 250–600 degrees Fahrenheit	6–9	Depends on type of oil, velocity, temperature, and flow design
Convection heating, pitch and compounds	1–3	Depends on material temperature and design details

For more critical jobs requiring extreme variation in heat input, other types of control are available. One of these is by time cycling, whereby the power is cut on and off in the proper time intervals to maintain the correct average rate of heat input. Another method is by voltage-changing apparatus utilizing variable reactors or other means of electrical impedance. This is very sensitive as the heat energy of an electric heater varies as the square of the voltage changes.

Each of the control methods described has definite advantages and disadvantages. Only careful analysis of each electric heating process can determine the most satisfactory and economical type of control. Under most conditions the cost will increase in the order in which the types of control are listed below, but so many variables affect costs that no fixed rules can apply.

1. On-and-off control—one step.
2. Multiple step control of heater sections.
3. Time cycling control.
4. Voltage changing control.

Some concrete examples of how electric heat can be profitably used for industrial process work are given in the following.

Vaporizing Dowtherm

Dowtherm is a useful heat transfer medium whereby vapor can be utilized at high temperature with far lower pressure than for steam of equal temperature. As an example, Dowtherm *A* vapor has a pressure of only 88 pounds per square inch gauge at 700 degrees Fahrenheit.

For vaporizing Dowtherm in moderate or small-sized units, electric heat is very satisfactory. A suitable pressure vessel with submerged tubes enclosing electric heaters can be constructed with all welded joints, for containing the fluid. Explosionproof terminal boxes on the heater tubes, and explosionproof controls make such a vaporizer safe for locations convenient to the work without hazard from inflammable vapors.

Hot Oil Heat Transfer

For temperatures up to 600 degrees Fahrenheit, hot oil can be recirculated by a suitable pump through an electric oil heater and a jacketed vessel or work kettle, at very low pressures. Usually not over 20 pounds per square inch gauge is required to maintain circulation at the proper velocity for correct heat transfer.

Systems of this kind in units of from 50 to 300 kw capacity are successfully used in many chemical processes. Such heaters are compact, well insulated, safe for hazardous locations and when properly designed with correct oil flow velocity, heat density, and automatic control, give years of reliable operation without interruption of service. They can be located close to the work.

Superheating Steam

Where moderate quantities of steam must be superheated electric heat can be applied so as to maintain very close temperature tolerances even with a great variation in volume of steam flow. The low British-thermal-unit input per pound of steam per degree of superheat makes this a critical design problem which can best be solved with electric heat and special sensitive controls.

Surface Heating of Materials

Infrared heat can be radiated and reflected to the surface of materials such as plastic sheets, fabrics, paper, metals, glass, and so forth, as they pass under, over, or between high-temperature electric resistance radiant heaters without actual contact with them.

Heavy duty heaters of this type can be built into kilns, conveyors, dryers, presses, rolls and machines of all kinds. They are compact, very rugged and safe for all commercial power services up to 600 volts.

It is impossible to make general comparisons between the operating cost of electric heat and fuel fired equipment. Only in a specific case can the many variable factors be evaluated. Due to thermal losses in converting fuel into electric power, electricity can never compete with fuel for general heating purposes but there is no direct ratio between fuel cost and electric heat cost.

Electric heat will cost more to one user than to another even in the same city, because electric power costs depend on the class of service, the demand curve and many other factors which vary greatly. Electricity can not be stored like other fuels and heavy peak loads for short periods mean added cost.

Very often electric heat actually reduces the unit cost of a manufactured product by improving quality, reducing waste, saving skilled labor, and avoiding hazards. In many processes no other fuel can produce the desired results.

The tremendous growth of electric heating in industry is proof that it has its logical place along with fuels. The heavy load must always fall on fuels, but, with more careful engineering analysis of industrial heating problems, an increasing number of logical uses for electric heating will be found. The future of electric heating will not depend on its cost relation to fuels but rather upon how well the responsible engineer analyzes and solves the intricate design problems incident to the full utilization of its inherent possibilities.

No Discussion

Irrigation Pumping with Grand Coulee Power

S. M. DENTON
ASSOCIATE AIEE

CENTRAL Washington is one of the few areas in the semiarid western states where the quantity of water flowing in the streams is more than sufficient to irrigate all the suitable arable land in the area. The Columbia river is characterized by high summer flow coexistent with the irrigation season.

The purposes of the Columbia Basin Project are irrigation of land, power development, and to a minor extent flood control and improved navigation. In its most compact form, the final plan for the development of the Columbia Basin comprises

1. The world's largest concrete dam (Grand Coulee Dam).
2. A reservoir (Franklin D. Roosevelt Lake), extending 151 miles to the Canadian boundary.
3. Two powerhouses, which will, when completed form a concentration of power greater than any in existence today.
4. Two interconnected switchyards.
5. The world's largest pumping plant.
6. An equalizing reservoir.
7. An extensive system of canals, tunnels, siphons, wasteways, control works, and miscellaneous appurtenances sufficient to irrigate about 1,000,000 acres of land in an area 60 miles wide and 80 miles long.

As the land to be irrigated lies on a plateau several hundred feet above the level of the Columbia river reservoir, water is to be pumped from the Columbia river reservoir to a reservoir formed by damming the two ends of the famous Grand Coulee, a remarkable prehistoric erosion channel cut to a depth of several hundred feet and from two to five miles wide through a 30 mile stretch of lava plateau. To serve the full Columbia Basin area, it will be necessary to raise a huge flow of water, about 16,000 cubic feet per second, through a lift ranging up to 365 feet. The power required for this work amounts to nearly a million horsepower.

Pumping Plant

The pumping plant will be located just upstream of the west abutment of the Grand Coulee dam and will contain an ultimate installation of twelve pumping units. The foundation for the pumping plant is a part of a wing dam which intersects and forms an angle of 67 degrees with the main dam. This part of the pumping plant structure has been completed. The pumping plant superstructure is expected to be completed in time to start the pumping of water early in 1951. The pumping plant structure will house the pumping units and pertinent controls.

The operation of ten pumping units will be required to meet maximum pumping requirements under the fully developed irrigation plan. Space has been allowed in the pumping plant building for the ultimate installation of 12 pumping units of which two are spare units. Standby units may be needed in the event of required maintenance on any component part of the generator-pump motor combination. Six pumping units will be installed initially with the first two scheduled for operation early in 1951.

To prevent pumped water from the balancing reservoir from flowing back through the pumps, the pump discharge line is designed with a siphon discharge outlet equipped with an air brake which will function when action is initiated by pump stoppage. When the pumping units are stopped by power failure or from other causes, the water in the discharge lines will return through the pumps and cause the pumping units to rotate in the reverse direction. Under conditions of maximum head, the pumping units may approach the maximum overspeed for which they have been designed.

A generator voltage bus structure will deliver power from the 108,000-kva generating units to the pump motors and will traverse the downstream slope of the Grand Coulee dam and occupy one of the galleries in the pumping plant building.

Pumps

Pumps of unprecedented size are being built for this installation. Each pump will lift 1,350 cubic feet of water per second its rated head of 310 feet from Roosevelt Lake to a canal feeding the balancing reservoir in the Grand Coulee. The pumps will operate through the pumping lift range of from 270 feet to 365 feet. The greater portion of the pumping will be through a lift of 270 feet when the reservoir is full. Under this condition the pumps will deliver in excess of 1,600 cubic feet per second. Varying river conditions cause the level of the reservoir to fluctuate as much as 95 feet. The pumps with intakes in the reservoir are placed below the lowest water level so that they will always operate under a positive pressure and avoid the risk of cavitation that is present when negative pressures occur. The size of these pumping units is indicated by the size of the pump impeller which is a steel casting fourteen feet in diameter. The impeller will be mounted on a 27-inch shaft for connection to the driving motor. Each pump will weigh approximately 500,000 pounds with the weight of the revolving parts about 120,000 pounds. Each pump will be supplied by an intake or suction tube fourteen feet in diameter which may be closed

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S. M. Denton is with the Bureau of Reclamation, United States Department of the Interior, Denver, Colo.

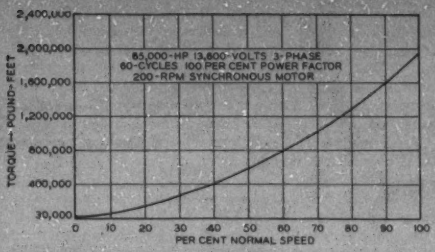

Figure 1. Torque requirements of motors

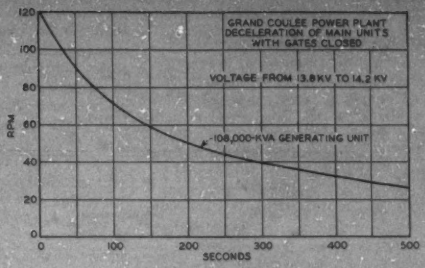

Figure 2. Deceleration curve of generating unit

off at the intake by a roller gate 13 feet by 20 feet in size. The discharge pipe from each pump is constructed of steel plate and is twelve feet in diameter. The discharge pipes are about 700 feet long and will be encased in concrete within a tunnel cut in the rock for approximately the lower two-thirds of the length of the pipe.

Power Plant

Eighteen main generating units comprise the ultimate installation at Grand Coulee power plant which is divided into two powerhouses, one located at each end of the dam. The generating units are each capable of carrying up to 130,000 kw under favorable hydraulic conditions. The dam, reservoir, and one powerhouse are substantially complete, and power has been produced since March 1941. Nine 108,000-kva generating units have been installed in the left powerhouse and are now in operation. The building for the right powerhouse is partially complete and will be ready to accommodate the installation of nine generating units which are scheduled for operation, three units each in 1949, 1950, and 1951.

Since the beginning of its operations in 1941, the power plant at Grand Coulee dam has carried a heavy load throughout the year and has been loaded to capacity almost continuously due to heavy demands from members of the Northwest Power Pool, a pool originally formed as a war measure to include the public and private utility systems in Washington, Oregon, Idaho, Montana, and Utah.

Description of Generating Units

The hydraulic turbine-driven generating units at Grand Coulee are the largest in existence. Six of the nine units now in operation will supply power for operation of the pumping plant. Each generator is direct-connected to a vertical shaft hydraulic turbine rated 150,000-horsepower at 330-foot hydraulic head and 120 rpm. The generators are rated at 108,000 kva, unit power factor, 3-phase 60-cycle and 13,800 volts. The normal excitation voltage is 250 volts. The generators to be used to furnish power for pumping have separate motor generator exciter sets to provide excitation during the reduced speed starting of the pump motors.

Generator characteristics influencing the pump motor design include the following:

Direct-axis transient reactance, 22.8 per cent.

Direct-axis subtransient reactance, 16.1 per cent.

Direct-axis transient short circuit time constant, 1.9 seconds.

Direct-axis transient open circuit time constant, approximately 8.66 seconds.

Direct-axis subtransient time constant, 0.54 seconds.

Armature resistance, 0.003651 ohms.

The combined turbine and generator WR^2 is 178,500,000 pound feet squared.

Design Features of Motors

Motors to drive the 1600-cubic feet per second pumps are of the synchronous type, each rated 65,000 horsepower at 200 rpm. They will operate at 13,600 volts, unity power factor, 3-phase 60-cycle, alternating current, and will be generally in accordance with the latest requirements of the American Standards Association. The motors are designed to rotate counterclockwise and to withstand the stresses resulting from operation at runaway speed of 295 rpm in either direction. The motors are of the vertical type with the thrust bearing located above the rotor supporting the rotating parts of the motor and pump. The weight of each pump runner and shaft and the unbalanced hydraulic thrust will be about 250,000 pounds. The weight of a complete pump motor is approximately 700,000 pounds, and the weight of the rotating parts will be in excess of 300,000 pounds. Each thrust bearing is equipped for establishing and maintaining an oil film during the starting cycle and until sufficient speed is attained to insure a full thickness of oil film. Each motor will have a separate motor-generator exciter set to furnish excitation during the starting operation. An enclosed cooling system is furnished with each motor.

In addition to motor breakaway torque, 30,000 pound feet is required to be developed by each motor to be applied to the breakaway of the pumps. The torques to be developed by each motor being accelerated through any speed will exceed that indicated by the speed torque curve shown on Figure 1. The WR^2 of the pump operating at 200 rpm with the pump casing full of water is 1,900,000 pound feet squared. The torques available for synchronizing one motor when connected singly to a generating unit at 45 per cent of normal speed and below, and at

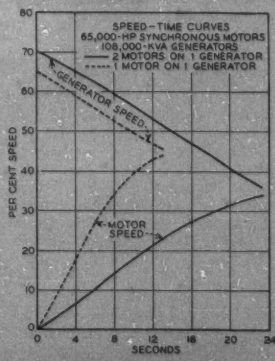

Figure 3. Estimated possible performance of pump motors and generating units

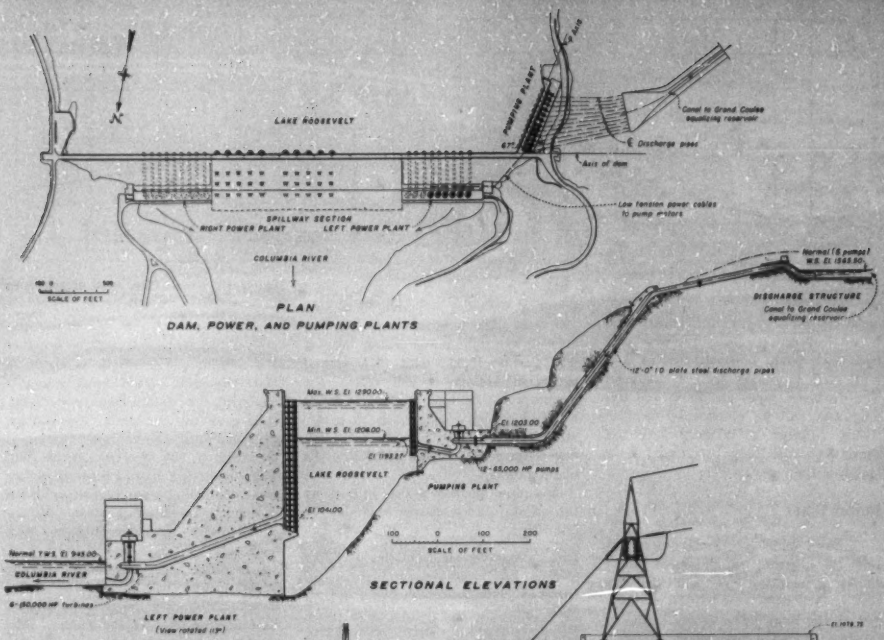

Figure 4. Diagram of pumping arrangement

35 per cent and below when two motors are connected to one generating unit, will exceed the torque requirement indicated by the speed torque curve. The pullout torque for each motor at normal voltage and speed is standard for a motor of this rating and will be not less than 150 per cent of full load torque.

Starting Methods for Motors

The unprecedented size of the pumps and motors also presented a new field of problems. Considerable study of these problems was required preliminary to issuance of specifications for the motors and generators. Consideration was given to the use of 60-cycle reduced voltage starting, but this method involved excessively high currents and high stresses in the windings of the connected machines. Therefore this method was discarded in favor of a scheme whereby one or both of a pair of pump motors will be started by a single generating unit at less than normal voltage and frequency. During normal operation, two motor-driven pumps will be connected directly to one generating unit through a generator volt-

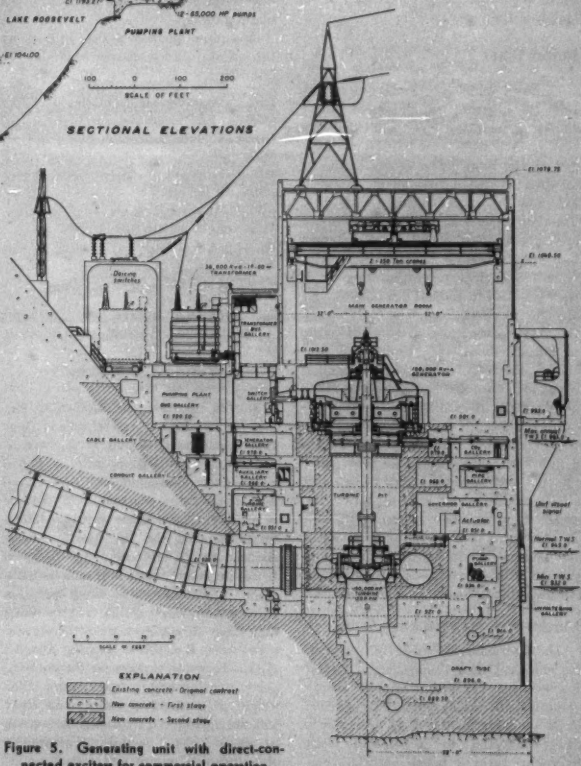

Figure 5. Generating unit with direct-connected exciters for commercial operation

age bus. The bus will be of the air insulated metal enclosed type with each conductor in a separate nonmagnetic housing. The longest of the bus structures will be about 1,700 feet, and the average length will be about 1,200 feet.

Two methods of starting have been considered in the designs of these motors.

1. The synchronous method in which motors are direct connected to the generators at standstill and brought up to normal speed in synchronism.

2. Starting as an induction motor with generating units operating at reduced speed when the motors are pulled into step.

It is planned to use the synchronous method exclusively unless operating conditions develop which would necessitate shorter starting operations. The synchronous method of starting will result in less shock to the generating and pumping units.

Synchronous Method

When the synchronous method of starting is used, one or two pump motors will be connected to one generator at standstill. The proper setting will be arranged for the generator and pump motor exciter field rheostats. Excitation will be applied to both the generator and pump motor fields simultaneously with the opening of the turbine gates. Adjustments can be made as desired in the field rheostats as the turbines start and accelerate. However, this will not be necessary under normal operating conditions. The generating unit and the pumping unit or units will be accelerated to normal 60-cycle speed by operation of the governor gate control causing the gates to open. At normal 60-cycle speed, the units will be placed under the control of the hydraulic governor. Starting and breakaway torque for both the motor and pump can be provided with this method of starting. The motors being furnished will have maximum starting torque above actual requirements to allow some variations from optimum starting conditions. The resistance of the armature circuits for both generator and motors plus the connecting bus is less than $1^1/_2$ per cent on the generator base. Higher resistance in the armature circuits would reduce the amount of starting torque available, and considerably higher resistance in the armature circuits might make this method of starting impractical.

The motor designs also allow for some variations in the strength of the generator and motor fields. The relative strength of the generator should normally be greater than that of the motor for optimum conditions. The ratio of the motor excitation to the generator excitation varies from approximately 0.6 to 1.0.

The measure of relative field strength is compared on the basis of open circuit voltages. The optimum starting torque condition will be obtained with a ratio of relative motor field strength to the relative generator field strength of approximately 0.8.

An optimum rate of gate opening is anticipated for the turbine driving the generator in the generator-motor combination. Gate openings faster than the optimum are expected to approach a rate at which the motors will be unable to follow. On the other hand, a rate of gate opening which is slower than the optimum is expected to have little adverse affect on starting operations.

Induction Motor Method

When induction motor starting is used, the speed of the generating units will be reduced to about 65 per cent of normal for starting one motor or 70 per cent for starting two motors. The fields of the motors and the generator will be unexcited. One or two pump motors will be connected to the generator by means of the generator voltage bus structure. With the generating unit rotating at the reduced speed, excitation will be applied to the generator field causing the motors to start as induction motors, to accelerate, and to pull into step with the generators at a decelerating generator speed. The generator and pump motor exciter field rheostats will be adjusted before starting to a setting which will be the most suitable to cover the entire starting and accelerating period. However, the generator exciter field rheostat may be adjusted during the starting operation to obtain improved starting conditions as the motors start and accelerate. The turbine gates will remain closed during the initial starting period. The load of the motor or motors starting will reduce the speed of the generators as the stored energy in the generating unit is transferred to the pumping units and as the motors start and accelerate. The motors and the generator to which they are connected will drift toward a common electrical speed and when sufficiently close, the motors and generator will be pulled into step by application of the motor field. The motor field excitation will be applied at or near the most favorable angle between the motors and generators. After the motors and generator are

Figure 6. Pumping plant site before completion of the dam showing tunnels for pump discharge pipes

Figure 7. Pumping plant foundation on wing dam shown in foreground

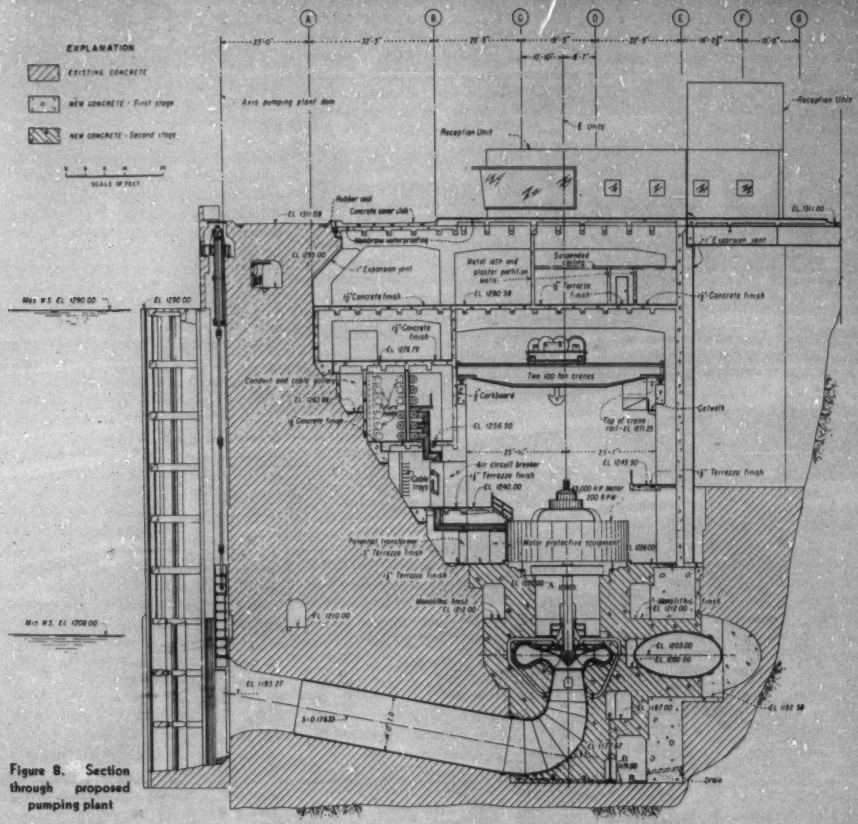

Figure 8. Section through proposed pumping plant

synchronized, the turbine gates will be opened to bring the generator motor or motors to normal speed. At normal 60-cycle speed the units will be placed under the control of the hydraulic governor.

The generator performance for this method may be approximated by using its zero power factor volt-ampere curve. This approximation is obtained by plotting the current which the motor or motors will draw at rated voltages on the same chart with the zero power factor volt-ampere curve of the generator. A line drawn from this point to the origin intersects the volt-ampere curve of the generator and gives current and voltage values which the generator can deliver. These values are conservative, however, since the power factor will actually be better than zero. The torque available would be reduced below rated torque by the ratio of the square of the reduced voltage to the square of the rated voltage of the motors. Figure 2 shows the average deceleration for the generating units which will be used for pumping. It is possible to estimate the action of the generator and its connected motor or motors when starting under the induction motor method. When starting one or two motors, the motors and generating unit may be expected to approximate the general type of performance indicated on Figure 3. Initial generating unit speeds for this method of starting may be less than those mentioned above and as shown on the curves. The minimum starting speed for the generating unit can be determined best by test as well as the most favorable speed for starting either one or two motors.

Operating Conditions

The output of a generating unit may vary from about 80,000 kw to 130,000 kw or more depending upon the amount of head available on the turbine. One or two pumps may be driven from one generating unit depending upon the pumping requirements and capabilities of the generating unit. Excess generating capacity over the normal rated loading of the pumps may be used by increasing the speed above the normal rated speed of the pumping units. The pumping units will be loaded approximately as the cube of the operating speed.

The generators assigned to irrigation

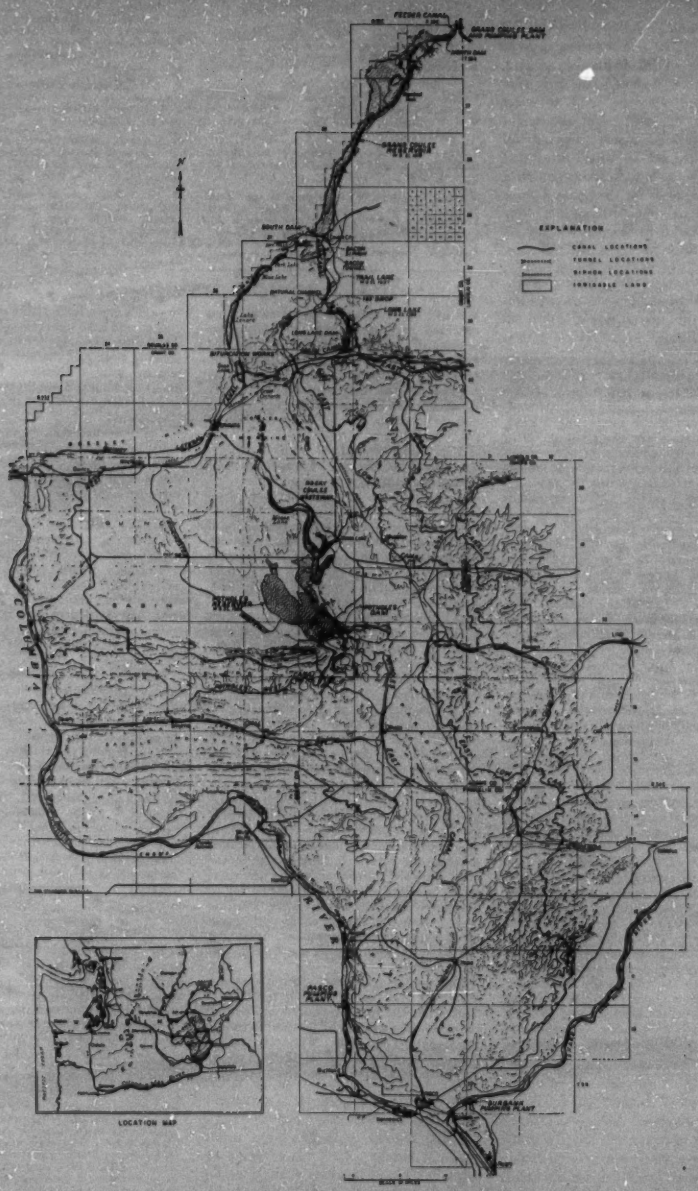

Figure 9. Irrigation system for Columbia Basin Project

pumping will be available for commercial service during the nonirrigation periods. By resorting to intermittent pumping, the generators assigned to irrigation pumping may also be used to meet daily peak demands even at the height of the irrigating season. The general plan for pump operation will be integrated in so far as possible with the commercial power requirements for the Grand Coulee power plant so that little or no interference will result in the power demands being met.

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Discussion

M. R. Lory (Westinghouse Electric Corporation, East Pittsburgh, Pa.): Denton has given a very interesting description of this outstanding engineering project of which almost every part is the largest of its kind. The pump motors are the most powerful ever built by a wide margin.

The problem of starting these large motors is one of the most interesting to engineers. Although there is no previous experience in starting motors of this size, we are still close enough to similar practices so that successful performance is assured. The synchronous starting of large machines is commonly done on the test floor of all manufacturers of electrical equipment. The largest generators are started by this method, using a driving generator much smaller in relation to the machine being started than will be the case at Grand Coulee.

The induction motor method of starting is exactly like the method used to maneuver the propeller in modern a-c ship propulsion installations. On shipboard, we have a generator equal in rating to the motor it drives. The maneuvering is done at reduced generator speed, with the motor acting as an induction motor until the motor approaches synchronous speed. On many twin-screw vessels we have the case of two motors being started from one generator. In these ship drives, tests check calculated performance closely and the same should hold true for the Grand Coulee motors.

Out of the six motors in the initial pumping installation, four are being furnished by the Westinghouse Electric Corporation. These machines are 31 feet, 3 inches, in diameter over the air housing, and extend 18 feet, 3 inches above the pump room floor. The total over-all height from the face of the shaft flange to the top of the tachometer housing is 24 feet, 2 inches. The total weight of each motor is 690,000 pounds, and the rotor weighs 348,500 pounds. The thrust is carried by a 56-inch diameter Kingsbury-type thrust bearing.

Because of the unprecedented size of these motors, the Bureau of Reclamation has specified many instruments and protective devices to detect abnormal conditions and give the operators complete information on operating conditions. I will not go into detail regarding all of these devices but it may be of interest to consider the instruments and devices provided for the protection of the thrust bearing.

The temperature of the oil in the thrust bearing pot is measured by a mercury thermometer and also by a remote reading, recording thermometer which keeps a continuous record. Resistance-type temperature detectors are located in two shoes of the bearing and a temperature relay also has its bulb in the metal of one bearing shoe and will sound an alarm if the temperature becomes excessive. The temperature of the cooling water for the bearings is measured by mercury thermometers in the inlet and outlet, and a pressure gage is located in the supply line. Failure of the cooling water is detected immediately by a pressure failure relay in the cooling water supply header.

Denton mentions that a means is provided to establish the oil film in the thrust bearing before starting the motors. This accomplishes the double purpose of reducing the starting torque, and eliminating any possibility of damaging the bearing during the first few revolutions. In the Westinghouse motors, the oil film is introduced by jacking up the rotor a short distance to permit oil to flow between the bearing surfaces, and then lowering the rotor. Jacking is done by the jacks ordinarily furnished to jack the rotor for disassembly of the thrust bearing and to brake the rotor to a stop when shutting down. However, special equipment is provided so that the operator can complete the raising and lowering operation quickly and conveniently.

To raise the rotor, the operator moves a switch at the control station to "raise." This starts the high pressure pump and at the same time, applies air pressure to the oil in the supply tank. The air pressure fills the piping with oil through a by-pass and check valve around the pump. As the jacks start to rise, a green light at the control station goes off and a red light comes on. When the jacks come in contact with the rotor, the pressure rises, and the high pressure pump lifts the rotor 1/4 inch. At this point, a limit switch shuts down the pump and lights an amber light to indicate that the lift cycle is completed.

The operator then moves the control switch to "lower," and the lowering is automatically accomplished. Air pressure above the oil in the tank is released, and a solenoid valve permits the return of the oil to the tank through a flow control which gives the proper lowering speed. When the rotor is down, the amber light goes off, and the pressure drops in the system. The drop in pressure opens a discharge valve to empty the jack cylinders quickly. When the jacks are down, the red light goes off, and the green light comes on, indicating that the motor is ready to start. At the same time, the discharge valve is closed to maintain the system full of oil.

The oil introduced between the bearing surfaces by jacking up the rotor does not squeeze out for some time. The operator can easily complete the starting cycle while the oil film is maintained. The jacks can be used for braking the rotor to a stop in the normal way except that air is not admitted directly to the cylinder but is admitted to the oil supply tank, and the pressure is transmitted to the cylinders by the oil.

These motors will not be in service for some time, but I hope that Denton or his associates will give us additional papers in the future, as the tests are made after installation and operating experience is obtained.

S. M. Denton: Lory has given some very interesting comments from the motor designer's point of view which are greatly appreciated. His discussion regarding the two starting methods relating these methods to commonly used procedures is reassuring. He also brings out some of the interesting details in the design and manufacture of motors of this size.

Development of the 230-Kv Ring Bus for Grand Coulee Power Plant

N. G. HOLMDAHL
MEMBER AIEE

C. L. KILLGORE
MEMBER AIEE

GRAND COULEE power plant at Coulee Dam, Washington, is the largest hydroelectric power development in the world and, when completed, will have an installed capacity of over 2,300,000 kw. This unprecedented concentration of generating capacity presented a real and worthwhile problem in the selection, design, and development of the most desirable bussing arrangement and was first presented in an earlier paper[1] by Hoard and Bills. This paper discusses the problems of determining the ultimate bussing arrangement for Grand Coulee power plant and presents the reasons for the final selection of a 230-kv ring bus.

Most present-day high-voltage transmission systems in this country use a solid bus arrangement, or slight variations from it, because of the availability of quick-clearing high-interrupting capacity circuit breakers. In fact the need for this type of bus arrangement has resulted in the continued development of high-voltage circuit breakers of increasing interrupting capacity. For all but the larger generating stations, presently available 230-kv circuit-breaker interrupting capacities of 3,500,000 kva permit the use of a solidly-connected high-voltage bus arrangement which gives a high degree of stability and operating flexibility. However, in the case of Grand Coulee power plant circuit breaker interrupting capacities of 3,500,000 kva were not sufficient to permit the use of a solid high-voltage bus. Two opposite approaches appeared feasible.

1. Reduce the magnitude of the short-circuit by bus sectionalizing or the use of bus-tie reactance.
2. Increase the circuit breaker interrupting capacity.

The final solution of this problem required extensive work on the network analyzer, design studies and cost analysis as well as field tests[2,3,4] at Coulee Dam to determine the maximum interrupting capacity 230-kv oil circuit breakers that could be developed at this time. The following four factors, were given major consideration in determining the ultimate bussing arrangement for Grand Coulee power plant.

1. Circuit breaker interrupting capacity.
2. System stability and relaying.
3. Simplicity and operating flexibility.
4. Cost.

Grand Coulee Power Plant

Grand Coulee power plant is located on the Columbia River approximately 90 miles west of the city of Spokane near the center of the state of Washington. The power plant is also near the electrical center of the Northwest Power Pool, which has a peaking capacity of over 3,500,000 kva, and extends over the states of Washington, Oregon, Idaho, Montana and Utah. Grand Coulee power plant is connected to the Northwest Power Pool through the transmission facilities of the Bonneville Power Administration and the development of this transmission system is described in a companion paper[5] by Hoard and Helwith.

The Grand Coulee power plant was designed, constructed and is operated by the United States Bureau of Reclamation as an integral part of its Columbia Basin Project. This is a multipurpose project which will eventually irrigate over 1,000,000 acres of land in central Washington, and have an installed hydroelectric generating capacity of over 2,000,000 kw. Grand Coulee consists of two power plants located on either side of the Columbia River as shown in Figure 1. Each power plant will ultimately contain nine of the world's largest hydroelectric generators,[6] shown in Figure 2, each having a nameplate rating of 108,000 kw with an actual peaking capability of 130,000 kw. At present all nine of the generators are installed in the left power plant, as shown in Figure 3. The nine generators for the right power plant are scheduled to be in operation by 1952.

A unit-system consisting of a generator and its associated transformer bank wherein all bussing is accomplished on the high-voltage side of the transformer banks at the 230- and 115-kv switchyards is used at Grand Coulee. Seven of the generators in the left power plant supply power at 230-kv, one generator supplies power at 115-kv and one generator is connected to a 3-winding transformer with full capacity 115- and 230-kv high-voltage windings. This bank serves as a tie between the 115- and 230-kv switchyards. All nine of the generators in the right power plant will supply power to the 230-kv system.

Irrigation Pumping Requirements

The Grand Coulee pumping plant, now under construction, is located adjacent to the dam and will ultimately contain twelve 1,600-cubic feet per second 65,000-horsepower irrigation pumps[7] which will lift the waters of Lake Roosevelt 295 feet to an irrigation system in the Columbia Basin. Six of the nine generating units in the left power plant are arranged to either drive the twelve pump

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N. G. HOLMDAHL is with the United States Bureau of Reclamation at Coulee Dam, Wash., and C. L. KILLGORE is with the Bureau at Denver, Colo.

The authors wish to acknowledge the excellent cooperation and assistance of the engineers of the Bonneville Power Administration, the General Electric Company and the Westinghouse Electric Corporation, in developing the 230-kv ring bus for Grand Coulee power plant. Without the vision of the manufacturers' engineers in undertaking the development of 10,000,000-kva 230-kv circuit breakers, the 230-kv solid-ring bus at Coulee Dam could not have become a reality.

Figure 1. Grand Coulee Dam and power plants, with 230-kv left switchyard in right background

motors or to supply commercial power. Each of the six generating units will operate two pumping units through a generator-voltage bus between the power plant and the pumping plant.

The irrigation season, which begins gradually in the spring, reaches a peak in mid-summer, and tapers off until completion in the fall, also occurs when the maximum secondary power is available for irrigation pumping, since this is the high water flow season in the Columbia River and the requirement for commercial power is less than during the winter months. Considerable water storage will be provided in the irrigation canal system so that irrigation pumping can normally be scheduled during off-peak load periods. However even with maximum available secondary power and off-peak pumping it must be possible to continue to supply all or part of the commercial load which is normally assigned to any generating units being used for irrigation pumping, and any bussing arrangement selected for Grand Coulee power plant must provide for this fundamental operating flexibility.

Bussing Arrangements

Four basic bussing arrangements were considered for Grand Coulee power plant.

1. Isolated units and lines.
2. Multiple-winding transformers.
3. Interleaved transmission lines.
4. 230-kv reactor and solid ring bus.

Isolated Units and Lines. Since 2,500,000 kva was the highest interrupting capacity, 230-kv oil circuit breakers available at the time the power plant was originally designed, it was necessary to adopt a bussing arrangement using isolated units and lines in order to limit the circuit breaker duty. This scheme consisted of a complete 230-kv double bus with two generators and two transmission lines isolated on a single bus section. The economical utilization of power with this scheme depended upon sufficient interconnections between load centers so that all generators could be fully loaded regardless of the system load distribution. As the transmission system developed, it soon became evident that this condition would not be realized and the isolated unit and line bussing arrangement was abandoned.

Multiple-Winding Transformers. With circuit-breaker interrupting capacity still considered a limiting factor, various bussing arrangements using multiple-winding transformers were studied. These schemes are discussed in a paper[8] by Bills and MacArthur, and are essentially a ring bus with the interconnection between bus sections being established through the transformer windings which provide a means for transferring power and maintaining synchronizing torque between bus sections. While the reactance of the transformer windings limits circuit breaker duty, it also limits the amount of power that can be transferred between bus sections. Further investigation of this scheme indicated that it would

Figure 2. Rotor and stator of 108,000-kva generators at Grand Coulee power plant which are the largest hydroelectric generators in the world

Figure 3. Grand Coulee left power plant with all nine 108,000-kva generators installed

Note first six generators used for pumping are provided with separate motor-driven exciters

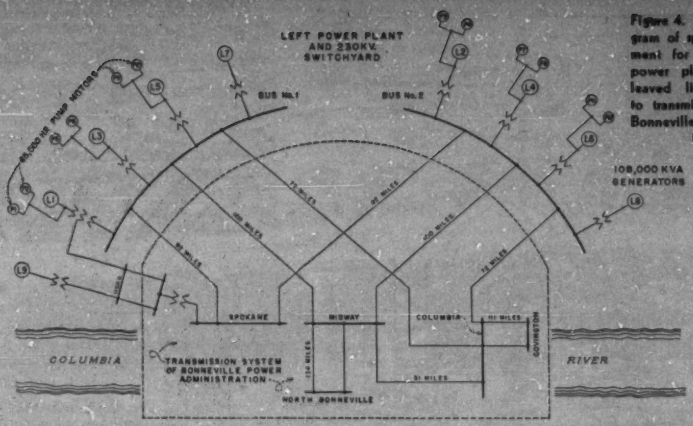

Figure 4. Schematic diagram of split bus arrangement for Grand Coulee power plant with interleaved line connections to transmission system of Bonneville Power Administration

be difficult to obtain the desired reactance between all transformer windings, that installation of large multiple-winding transformers in the existing power plant would be difficult, and that the cost of such an installation would be high. In view of these facts and the limitation in operating flexibility imposed by the necessity of transferring power through multiple-winding transformers this scheme was discarded.

Interleaved Transmission Lines. When higher interrupting capacity circuit breakers became available the existing line circuit breakers at Grand Coulee power plant were rebuilt from 2,500,000 to 3,500,000 kva. However, with the installation of only four of the ultimate 18 generators and three transmission lines, this interrupting capacity was exceeded. In order to limit circuit breaker duty, a bussing arrangement having two isolated bus sections with interleaved transmission lines between the nearest substations was adopted for Grand Coulee power plant. A simplified schematic diagram of this bussing arrangement is shown in Figure 4. It will be noted that two parallel transmission lines are installed between Grand Coulee power plant and each substation on the transmission system. One transmission line of each pair of parallel lines is connected to each bus section at Grand Coulee and the lines are connected together at the substations. Half the generation at Grand Coulee is connected to each bus section and synchronizing power between the two bus sections flows through the interleaved transmission lines and substation busses with line reactance limiting circuit breaker duty.

This bussing arrangement seriously limits system operating flexibility since power cannot readily be transferred from one load area to another except through the interleaved transmission lines, and it is impossible to predict line loadings for various loads in the different areas on the system. In case of a system disturbance even with only six generators and six lines in service, the operators have difficulty in determining what has occurred or how the line loadings will redistribute. In addition, line relaying problems are greater with interleaved lines, particularly in regard to induced voltages in the parallel lines. Because of these limitations in operating flexibility the interleaved transmission line bussing arrangement was also abandoned. Six of the existing circuit breakers at Grand Coulee power plant have been rebuilt to 7,500,000 kva to permit changing over from the interleaved scheme to a solid bussing arrangement for the left power plant and 230-kv switchyard by the fall of 1948. This interim scheme of operation with eight generators and six transmission lines solidly connected together was necessary to provide sufficient operating flexibility to supply the changing load areas and will be used until the 230-kv ring bus is installed in 1949.

230-Kv Reactor and Solid Ring Bus. Figure 5 shows a simplified schematic diagram of the 230-kv ring bus which was finally selected as the ultimate bussing arrangement for Grand Coulee power plant since it offered the greatest advantages of any scheme considered. Essentially, the ring bus scheme consists of connecting the main busses in the 230-kv left and right switchyards together with two tie circuits crossing the Columbia River to form a closed ring to which all generators and lines are connected. The ring bus is divided into five sections through a bus-tie circuit breaker with each section containing three generators and two transmission lines so that in case of a bus fault only 20 per cent of the generation and line capacity is lost.

With a 230-kv ring bus arrangement the output of Grand Coulee power plant will automatically be transmitted to the load areas where it is most needed up to the capability of the outgoing transmission lines. Power can be transmitted in on some lines and out on others if an excess of generation exists in one area and there is a shortage of power in another. During the irrigation season when a maximum of five generators are being used for irrigation pumping and are off the commercial system power can be routed around the ring to the bus sections and their associated transmission lines from which the generators have been removed for irrigation pumping. A 230-kv ring bus appears to be particularly well adapted to the condition of operation at Grand Coulee power plant, and although it requires somewhat more initial capital investment, the advantages of greater operating flexibility and ease of maintenance are considered to more than offset the additional first cost. Variations of the 230-kv ring bus which were considered are as follows

1. A solid ring bus.
2. A ring bus with three 230-kv reactors to limit circuit breaker interrupting ca-

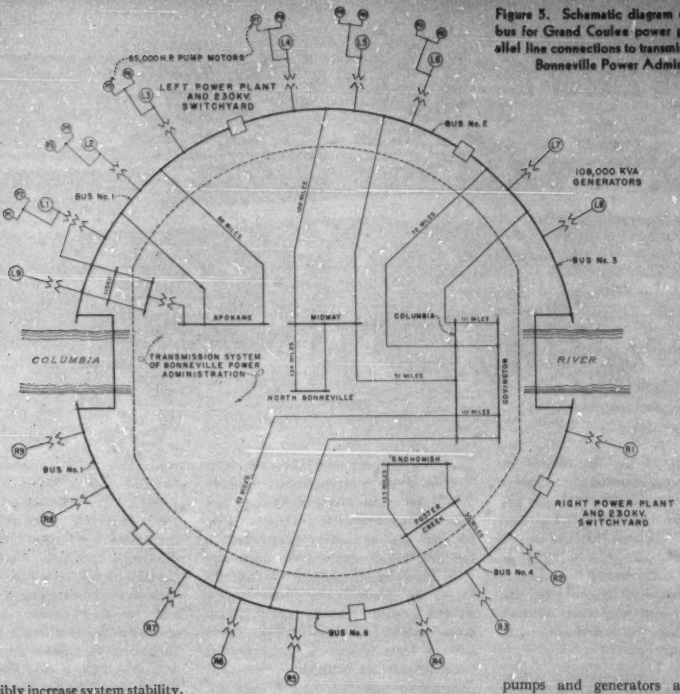

Figure 5. Schematic diagram of 230-kv ring bus for Grand Coulee power plant with parallel line connections to transmission system of Bonneville Power Administration

pacity and possibly increase system stability.

3. A ring bus with five 230-kv reactors located adjacent to each bus-tie circuit breaker.

Since 230-kv reactors are relatively expensive and constitute a limitation in the amount of power that can be transferred around the ring bus, it was decided to consider a solid ring bus which meant that 230-kv circuit breakers with much higher interrupting capacity than the presently available 3,500,000 kva would be required.

Circuit Breaker Interrupting Capacity

With all generators, pumps, and lines in operation, calculations indicate that it ultimately will be theoretically possible to obtain a maximum 3-phase fault of approximately 12,000,000 kva on a transmission line adjacent to the 230-kv switchyard bus.

Decrement Curves. The Grand Coulee generators have a low subtransient reactance of 16 per cent and, therefore, contribute a considerable d-c component to the total fault current for faults close to the 230-kv switchyard. Figure 6 shows the ratio of total rms current in per unit of initial symmetrical 3-phase short-circuit current from actual field tests to be approximately 1.4 for faults supplied entirely by the generators without any feedback from transmission lines, while the ratio with an equal number of generators and transmission lines is 1.2. The circuit breaker contacts are assumed to part at 2.25 cycles after the fault is initiated. An average ratio of 1.35 was used for the ultimate installation.

Pumps Isolated. To avoid increasing the interrupting duty on the generator-voltage circuit breakers used for the pump motors it was decided that the irrigation pump motors and their associated generators would not be operated in parallel with the commercial power system. However, each pair of pumps, when operating at rated speed and head, will not require the full capacity of their associated generators, and in order to utilize the available generator output it is planned to operate the pumps and generators at about 109 per cent rated speed. Under conditions of normal tailwater and maximum lake elevation it is expected that nearly 1,900 cubic feet per second will be obtained from each pump.

Circuit Breaker Interrupting Capacity. It was believed that the highest interrupting capacity 230-kv circuit breakers which it was possible to obtain from the manufacturers should be used for the 230-kv ring bus. In order to determine the maximum possible interrupting capacity circuit breaker that could be developed at this time, it was necessary to conduct a series of field tests at Coulee Dam. In view of the very successful results of these tests as reported in previous papers[2,3,4] it was determined that 3-cycle 230-kv oil circuit breakers with 10,000,000-kva interrupting capacity could be built for the Grand Coulee power plant. These circuit breakers were required also to have a continuous current carrying capacity of 1,000 amperes and to have a rating of 10,000,000-kva with one 20-cycle reclosure. Contracts have been awarded for fifteen new 230/196-kv 10,000,000-kva circuit

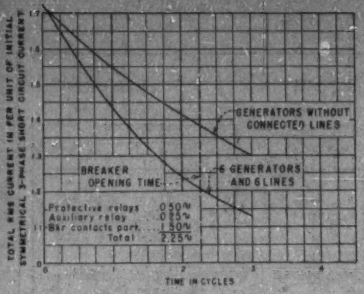

Figure 6 (left). Decrement curves for faults on 230-kv bus at Grand Coulee switchyards

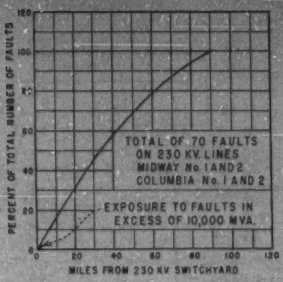

Figure 9. System exposure to faults from operating records for years 1945–1947

breakers and for the necessary parts to rebuild six of the existing 3,500,000-kva line circuit breakers to 10,000,000-kva interrupting capacity.

Maximum Theoretical Fault and Experience. While calculations show that it ultimately will be theoretically possible to obtain faults in excess of 10,000,000 kva, it is believed that this interrupting capacity is adequate for the ultimate installation at Grand Coulee power plant as discussed below. Operating records for Grand Coulee power plant from 1941 to 1948 show that 69 per cent of all line faults were line-to-ground, 29.9 per cent were phase-to-phase and only two or 1.1 per cent were 3-phase faults. Since line-to-ground faults with the neutrals of the transformer banks solidly grounded will increase the theoretically possible 3-phase fault of 12,000,000 kva by approximately 30 per cent, reactors will be installed in the neutrals of the transformer banks to limit the line-to-ground faults to 10,000,000 kva when increased system capacity makes this necessary. The addition of these neutral reactors will still permit the system to be classed as solidly grounded since the ratio of the zero sequence reactance X_0 to the positive sequence reactance X_1 will be less than three which will permit the application of line-to-ground rated lightning arresters. With the line-to-ground faults limited to 10,000,000 kva the following simultaneous conditions must occur to obtain a fault in excess of that value.

1. The fault must be a 3-phase fault, and must be a solid fault without any resistance.
2. The fault must be on a transmission line within a radius of about one mile from the switchyards. The bus-tie circuit breakers will never be required to open 10,000,000 kva.
3. All generators must be in operation and maximum feedback must be obtained from the transmission system. This assumes no irrigation pumping and no generators out for maintenance.
4. The fault current must be practically fully offset, which is only obtained when the fault is applied at voltage zero.

Therefore, the probability of obtaining a fault in excess of 10,000,000 kva is extremely remote and even if it did occur the bus-tie circuit breakers will provide back-up protection in case a line circuit breaker fails to clear the fault.

Experience records at Grand Coulee show that while it was theoretically possible to have a fault of 3,400,000 kva, the necessary simultaneous conditions as previously listed did not occur and, therefore, the maximum circuit breaker duty actually did not exceed 1,900,000 kva. Also the majority of the faults were less than half of the theoretically possible maximum as shown on Figure 7. Only 10 per cent of the faults were more than 50 per cent offset at zero time as shown on Figure 8. The actual system exposure to faults for the years 1945–1947 is shown on Figure 9. Circuit breaker duty decreases very rapidly with the addition of only a few miles of transmission line as shown in Figure 10. This is especially true at Grand Coulee power plant since the few extra ohms resistance associated with a few miles of transmission line results in a small (fast) d-c time constant which acts to reduce a large proportion of the fault current which comes from the generators. In addition the transmission lines radiating from Grand Coulee are protected by overhead groundwires for approximately one-half a mile from the switchyard. From the foregoing it can be concluded that for all practical purposes 10,000,000-kva interrupting capacity will be ade-

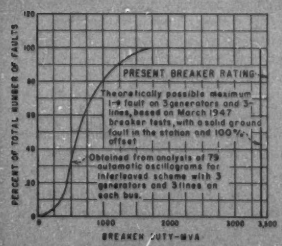

Figure 7. Circuit breaker duty for interleaved bus operation from system records for years 1944–1947

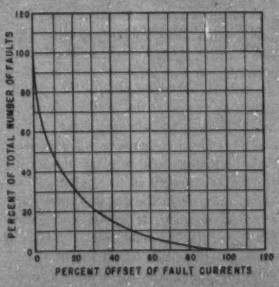

Figure 8. Per cent offset of fault currents obtained from automatic oscillograms for years 1946–1947

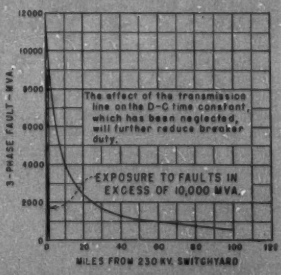

Figure 10. Typical effect of line length on circuit breaker duty at Grand Coulee switchyards

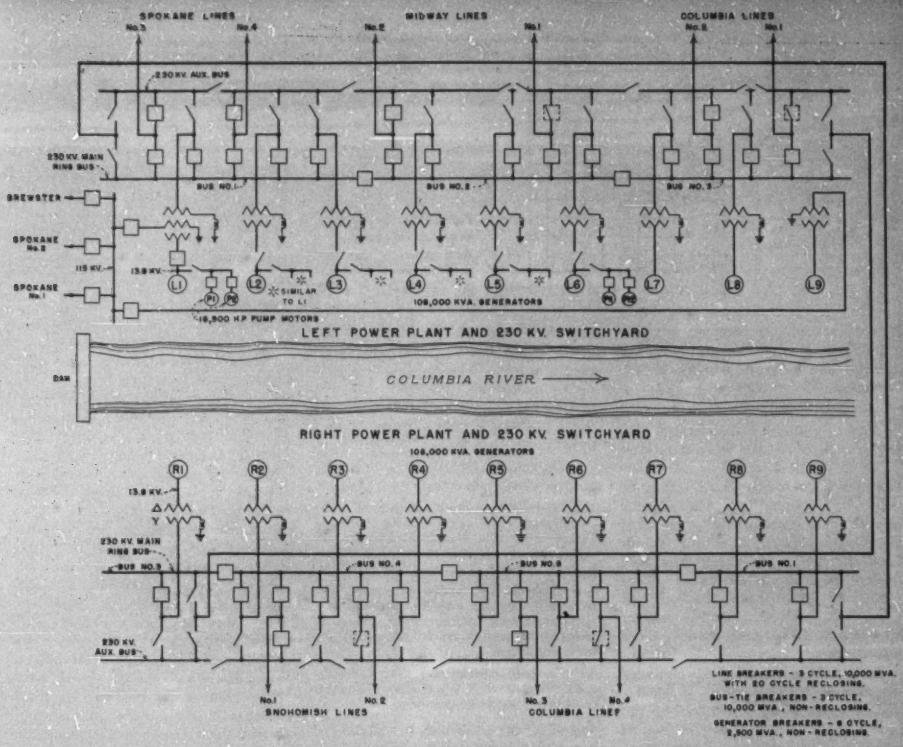

Figure 11. Physical arrangement of ultimate 230-kv main ring and auxiliary bus for Grand Coulee power plant

quate for the ultimate installation at Grand Coulee power plant, especially since it is reasonable to consider that the circuit breakers will have some margin.

Stability

The criteria for stability of the transmission system as established by the Bonneville Power Administration required that the system should be stable for a 3-phase fault at the Coulee 230-kv bus, and simultaneous loss of two parallel 230-kv transmission lines supplying the same load area. While these are rather severe stability requirements, the reasons for their selection and other aspects of system stability are fully discussed in a companion paper.[5]

However, it is desired to point out that this criteria for stability can be adequately met by a solidly-connected 230-kv ring bus, and network analyzer studies indicate that there is little difference in this stability criteria between an interleaved bus scheme, a 230-kv ring bus with low reactance between bus sections and a 230-kv solid-ring bus. For faults other than a 3-phase fault on a transmission line adjacent to the 230-kv switchyard bus the ratio of the critical switching times for the 230-kv solid-ring bus to that of the switching times for the other bussing arrangements will increase, and the 230-kv solid-ring bus results in the higher stability as the fault location moves away from the 230-kv switchyard bus. Accordingly, it can be concluded that the solidly-connected ring-bus scheme will result in greater over-all system stability for the more frequent fault conditions than that which can be obtained with an interleaved or 230-kv reactor ring-bus arrangement.

Operation and Flexibility

Since the Grand Coulee power plant is very near the electrical center of the Northwest Power Pool and is the largest single plant on the system, it is essential that the bussing arrangement be capable of providing adequate switching flexibility to route the output of the power plant to high load areas. Power may be routed by displacement through the switchyards from the coast area to eastern Washington and Montana or vice versa when power shortages occur in areas that cannot be entirely supplied by Grand Coulee.

General Operating Requirements. The general operating requirements for Grand Coulee power plant, all of which are adequately met by the 230-kv solid-ring bus, are as follows:

1. The switchyard configuration should be

such that it will be possible to transmit the total output of 17 108,000-kva generators (one spare) over ten 230-kv and five 115-kv transmission lines with the load in each line varying between zero and the maximum allowable stable loading.

2. The bussing arrangement should be capable of transferring power through the bus from a high-generation area to a high-load area up to the capability of the transmission lines.

3. A similar condition must also be met when five generators are off the system for pumping during the irrigation season and large blocks of power must be transferred from one bus section to another.

4. The bussing arrangement should be such that any piece of equipment may be removed for maintenance without impairing the power transmission capabilities.

5. The bussing arrangement should also be as simple as possible to provide ease of operation and should preferably be symmetrical.

6. The bussing arrangement should be such that the system will not lose synchronism with the simultaneous loss of two parallel transmission lines for a 3-phase fault at the 230-kv switchyard bus.

7. It must be arranged so that present available relaying equipment will provide adequate protection of the transmission lines without being unduly complicated.

Flexibility of 230-kv Solid-Ring Bus. It is evident that the greatest ease and flexibility of station and system operation will be obtained when the high-voltage bus is solidly connected.[9] For this case the power flow will be such that the load is delivered with the smallest electrical angular displacement between generator and load and the smallest amount of kilovolt-amperes for a given line capacity. System planning and development can progress naturally by strengthening the line capacity to the more heavily-loaded areas and additional capacity at the generating bus can be easily added. The increase in system kilovolt-ampere requirements following the loss of any line section is kept to a minimum as the increased loading will distribute more equally over the interconnected lines and the available synchronous capacity of the system will be better able to contribute a greater proportionate share of their available capacity. The required system synchronous condenser capacity will, therefore, be less with the solidly-connected ring bus.

Arrangement of 230-kv Ring Bus

The two power plants and their associated 230-kv switchyards for the Grand Coulee power development are located on opposite sides of the Columbia River as indicated on the single-line diagram, Figure 11. The existing 230-kv left switchyard is shown in Figure 12 and will contain 12 10,000,000-kva interrupting capacity conventional tank-type oil circuit breakers, as shown in Figure 13, in the bus-tie and line positions when the initial ring bus installation is completed. Figure 14 shows a transverse section through a 10,000,000-kva line circuit breaker position in the 230-kv right switchyard which is now under construction. This switchyard will eventually contain 9 10,000,000-kva interrupting capacity oil circuit breakers in the bus-tie and line positions. These circuit breakers will be of the low-oil content impulse-type and will be furnished complete with disconnecting switches and bushing-type current transformers as illustrated in Figure 15.

230-Kv Ring and Auxiliary Bus. Each switchyard has a 230-kv main ring bus and an auxiliary bus. The two main busses in each switchyard will be connected together by two cross-river tie circuits which will close the ring. As previously mentioned, the 230-kv main ring bus will be divided into five sections through bus-tie circuit breakers with three generators and two transmission lines connected to each bus section, except bus section number 1 which will have five generators since additional transmission lines may eventually be connected to this bus section. Space is provided at each bus-tie circuit breaker position for the future addition of 230-kv reactors if experience indicates that these are needed and they can be economically justified. A 230-kv auxiliary bus is provided for each main bus section except the cross-river tie circuits are omitted. The auxiliary bus is provided primarily for use during emergency conditions and during maintenance of the 230-kv main ring bus and its associated oil circuit breakers. The auxiliary bus is sectionalized with disconnecting-switches as shown in Figure 11 to provide the maximum operating flexibility in transferring generators from one bus section to another.

Circuit Breaker Arrangement. It will be noted from Figure 11 that double-breakers are shown in alternate line positions only, except for one pair of lines where double-breakers in both line positions were considered advisable since these lines are subject to more frequent outages than the other lines and are

Figure 12. 230-kv left switchyard

Ultimate installation will contain twelve 10,000,000-kva tank-type oil circuit breakers

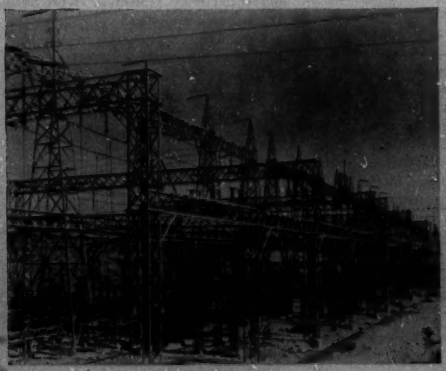

Figure 13. 10,000,000-kva 3-cycle, 20-cycle reclosing conventional tank-type oil circuit-breaker for 230-kv left switchyard

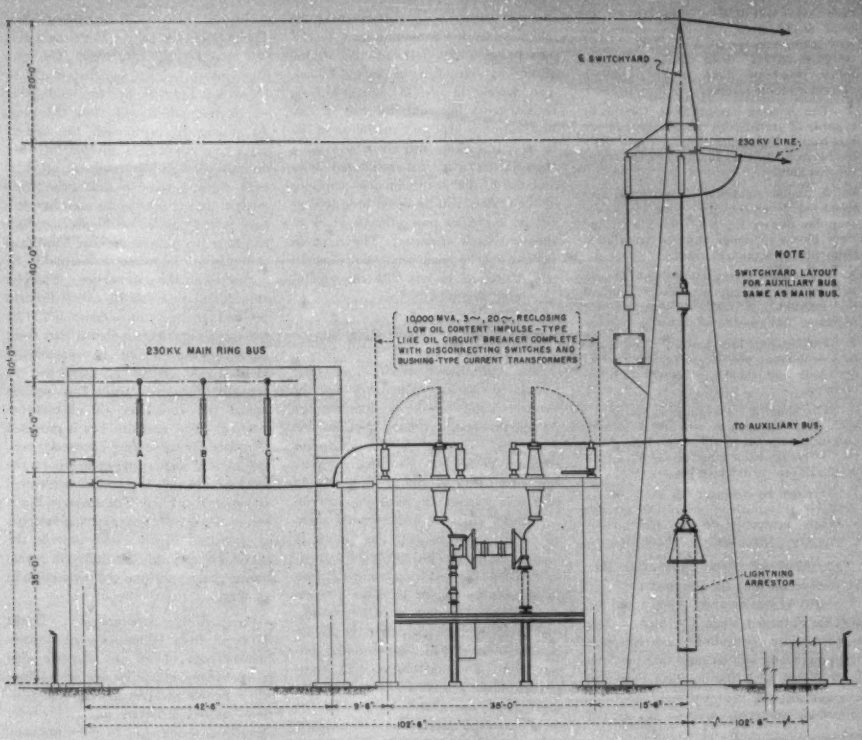

Figure 14. 230-kv right switchyard

Transverse section of 10,000,000-kva line circuit breaker position

installed on double-circuit towers. Double-breakers are shown in the alternate line positions since this locates a spare breaker where it is needed most. It is well recognized that line breakers are subjected to much greater duty from interruption of line faults and line charging current than generating unit breakers which are normally subjected to only routine switching operations. The spare line breaker on the auxiliary bus can be substituted for either of the two line breakers on the main bus or for any of the three generating unit breakers on its associated bus section. Space is provided for the future installation of double-breakers in all the line positions if operating experience indicates that this is desirable.

Only single circuit breakers will be installed for each generating unit position. These circuit breakers will not require an interrupting capacity of 10,000,000 kva since it is planned to trip the bus-tie circuit breakers in case of transformer differential relay operation, which could involve a high capacity 230-kv fault, before the generating unit circuit breakers are tripped. In view of the reliability of present-day transformers tripping the bus-tie circuit breakers for the remote possibility of a transformer fault is not considered to be a disadvantage. Since faults on the low-voltage side of the transformers will be limited by the transformer reactance, an interrupting capacity of 2,500,000 kva is ample for the generating-unit oil circuit breakers. Thus, a considerable saving was made by being able to use the existing generating-unit circuit breakers for the ultimate 230-kv solid-ring bus.

Relay Protection. The relay protection scheme for a typical section of the 230-kv main ring and auxiliary bus and its associated generators, transformers, and transmission lines is shown in Figure 16. Complete overlapping differential relay protection is provided for all station equipment. The generator and transformer protection is the conventional differential relay type. The transformer differential zone on the high-voltage side extends only to the current-transformers in the high-voltage bushings which provides differential relay protection whether the transformer is connected to the 230-kv main ring bus through the generating-unit circuit breaker or to the 230-kv auxiliary bus through a disconnecting switch. The 230-kv bus differential zone extends from the transformer high-voltage bushings to each transmission line and includes the 230-kv transformer circuits and both the 230-kv main and auxiliary busses. This scheme provides complete bus differential relay protection under any

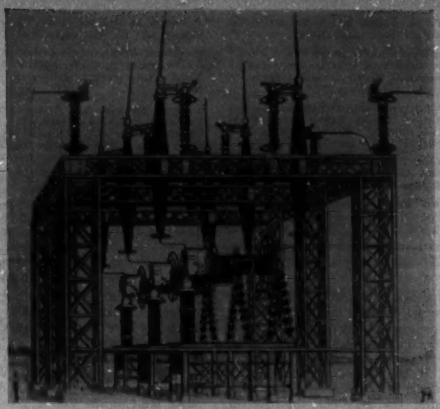

Figure 15. Drawing of 10,000,000-kva low-oil content impulse-type oil circuit breakers for 230-kv right switchyard

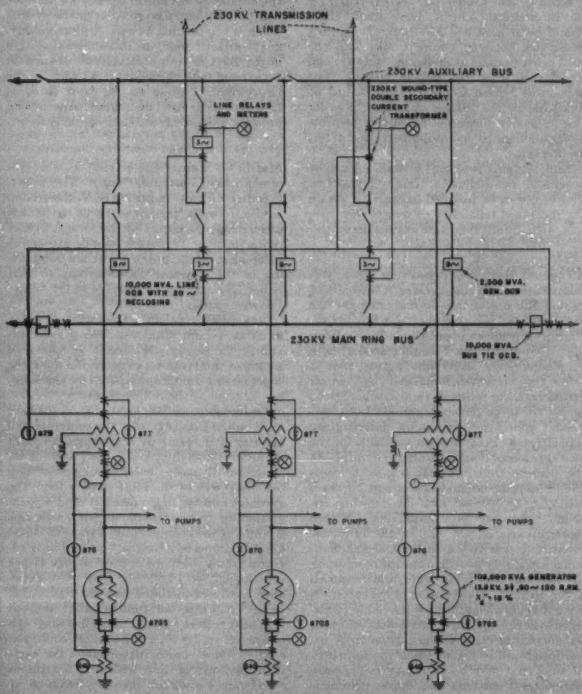

Figure 16. Single-line diagram of relay protective scheme for section of 230-kv main ring and auxiliary bus

switching condition by transferring trip circuits only. It will be noted that a 230-kv wound-type double-secondary current transformer is shown in the position provided for the future line circuit breakers. Installation of this current transformer permits completion of the bus and line current transformer circuits so that the normal protective relaying is always in service. With two parallel transmission lines between Grand Coulee power plant and the first substation on the transmission system the simplest type of line relaying can be used which will increase the reliability of system operations.

Conclusions

Selection of a 230-kv solid-ring bus for Grand Coulee power plant was based on the following conclusions:

1. Maximum system operating flexibility with a symmetrical switching arrangement is obtained with a 5-section 230-kv solid ring bus.

2. Maximum over-all system stability is obtained by the solid bus arrangement when using present-day high-speed circuit breakers and relays.

3. The most important means for improved performance and simplicity of bussing arrangements is the use of higher interrupting capacity circuit breakers.[10]

4. If it can be shown that installation of reactors in the 230-kv ring bus will increase the power limit of the transmission system by a sufficient amount, then it may be economically justified to install reactors.

5. However, it must be recognized that such bus-tie reactors impose a limitation on the transfer of power around the ring bus and should have as low reactance as possible and a continuous current rating ample to meet emergency conditions involving line outages.

6. By use of a ring bus additional generation and corresponding line capacity can be easily added by adding bus sections.

7. A main ring bus and a sectionalized auxiliary bus with double circuit breakers in alternate line positions provides the greatest operating flexibility and ease of maintenance at the least cost.

8. The 230-kv ring bus with two parallel lines to each load center permits the simplest type of relaying, thereby increasing the reliability of operation.

In summary it can be concluded that the 230-kv solid-ring bus which has been developed for Grand Coulee power plant will provide the required characteristics of flexibility, simplicity of operation, ease of maintenance, maximum stability, and simplicity of relaying at the most reasonable cost for operation of the largest concentration of hydroelectric generating capacity in the world.

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Discussion

S. B. Crary (General Electric Company, Schenectady, N. Y.): This paper is an excellent presentation of the advantages of high-interrupting capacity circuit breakers to a major transmission system. High-interrupting capacity is a development which naturally follows quick fault clearing. The next step, already being achieved, as indicated in this paper and a paper by B. V. Hoard and E. E. Helwith,[1] is a heavier electrical loading of the transmission line circuits—all logical steps toward a more economic system.

One cannot help but be impressed by the increasing need in this country and in Europe for quick-clearing high-interrupting capacity circuit breakers. The development of such circuit breakers in this country is particularly timely as it sets a pattern, not only for American but also for European engineers just when they are planning to redesign their systems, and go to higher voltages in many cases.

These recent circuit breaker developments will provide a means for superimposing a higher voltage transmission system on an existing system of limited transmission capacity. This places the responsibility for the primary transmission on the modern higher voltage system and will allow the older lower voltage system to provide subtransmission capacity with a minimum revamping of the lower voltage system, which in many cases is inadequately designed for bulk power transmission with reliability.

Reference

1. See reference 5 of the paper.

S. B. Smith (Ebasco Services Incorporated, New York, N. Y.): The paper by Holmdahl and Killgore presents a 230-kv ring bus arrangement which permits flexibility not only in operation but in future transmission line connection rearrangement, when and if this is desirable. The connecting of two parallel lines to the same bus section, as set forth in their plan, does not give as great reliability of service in case of bus faults as would an interleaved scheme in which the continuous ring bus is maintained with each circuit of a pair of parallel lines connected to different bus sections.

Because of the Northwest Power Pool interconnections and the large amount of generating capacity with which Grand Coulee will normally operate in parallel, the loss of one bus section with its three connected generators at Grand Coulee could at least be partially, if not entirely, compensated for by automatically increased generation at other interconnected plants. The greatest advantage can be obtained from this relief if the various 230-kv busses throughout the interconnected power system maintain the maximum number of interties throughout the transient disturbance. This cannot be done if, for example, two parallel lines to Midway, or to Spokane, or to Snohomish are lost simultaneously when a Grand Coulee bus section trips. If an interleaved scheme were used, there would be at least one 230-kv line remaining in service between Grand Coulee and the load center busses at Spokane, Midway, and Snohomish after the loss of any one bus section at Grand Coulee. Time would be available to the system operators to order the necessary measures to correct possible overloaded lines or equipment after a bus section trips.

Line relaying problems introduced can be accepted, or additional line relaying investment can be justified in order to obtain the increased reliability of service of a ring bus with interleaved line connections.

Alexander Dovjikov (Bonneville Power Administration, Portland, Oreg.): Instead of calling the use of bus-tie reactance on one hand and the increase of circuit breaker interrupting capacity on the other as two opposite approaches, I would consider them supplementary. Everyone agrees that a solid ring bus (sectionalized by circuit breakers) is the most convenient scheme and its use for large generating power house depends upon the maximum interrupting capacities of circuit breaker available for the interruption of maximum short circuits. At the present time the manufacturing companies have agreed to build circuit breakers rated at 10,000,000 kva interrupting capacity. Insofar as calculations indicate short circuits on the bus of the fully developed system may exceed 10,000,000 kva, a place is wisely provided for the reactors in the bus described in the paper, and inasmuch as the ultimate development of the system is several years ahead, time is available to observe in actual operation the factors affecting the maximum value of the current to be interrupted and to decide upon the measures (if any) that may be necessary to decrease system fault currents to a degree commensurate with reliable operation of the circuit breakers without excessive maintenance. Whether it should be done by insertion of reactance in the neutral of the transformer or by use of bus reactors will be determined by accumulated experience and the crystallization of ideas in regard to the decrease of insulation level.

I would also like to state that the criteria for stability of the transmission system, "that the system should be stable for a 3-phase fault at the Coulee 230-kv bus and simultaneous loss of two parallel 230-kv transmission lines supplying the same load area," refers to the time when, to canalize the full output generated at Grand Coulee, the number of circuits in the 230-kv network will be sufficiently large to keep the system stable, even in such a case. Unfortunately, at the present time, the transmission system of the BPA is yet not developed to such an extent and faults accompanied by loss of the two circuits leading to the same load, as demonstrated by our recent experiences, produce dynamic instability of the system.

Martin J. Lantz (Bonneville Power Administration, Portland, Oreg.): This paper, together with the paper by B. V. Hoard and E. E. Helwith, presents two plans for the ultimate development of the Grand Coulee power plant. The plans for the future development of the power plant are progressing in the physical plant layout, so that either scheme may be adopted as the future system expands.

One major problem in the future development of Grand Coulee is the present inability to secure oil circuit breakers of sufficient interrupting capacity to meet the short circuit duties anticipated by the solid-bus scheme without bus reactors.

Preliminary calculations for a 3-phase fault on the Coulee bus for the contemplated 1956 system result in a symmetrical fault value of 12,000,000 kva. This system includes the ultimate Coulee development with 5,000,000 kva of the fault coming from the system exclusive of Coulee. If solid neutral grounding is continued on the solid-bus scheme, the symmetrical value of fault kilovolt-amperes for a single-phase-to-ground fault would be approximately 15 per cent higher, or 14,000,000 kva. No allowance has been included in these figures for increased values of fault current due to transient offset which may increase the above values by an estimated 20 to 30 per cent. These fault magnitudes are considerably out of line with the maximum rated values of circuit breakers available at this time. Circuit breakers to meet these large values of fault kilovolt-amperes may be available by the time required; however circuit breaker rebuilding may be required

a reactor or bus segregation scheme is not adopted.

It is proposed to reduce the single-phase-to-ground fault magnitude of the solid bus scheme to 10,000,000 kva by the use of transformer neutral reactors. This means a further method is required to reduce 3-phase faults to the capabilities of present circuit breakers, or the interrupting rating of the circuit breakers must be increased.

Consideration is now being given to reducing the Bonneville Power Administration system insulation level in various locations. One of the main savings to be made by this reduction is in the cost of the transformer insulation.

Transformers operating at a system location whose ratio of zero-sequence reactance to positive-sequence reactance or X_0/X_1 is more than three, require full insulation, and protection by full-voltage rated lightning arresters. System locations having a ratio of X_0/X_1 of less than three may have 80 per cent lightning arresters and system or transformer insulation in the 196-kv class for a 230-kv system. With very low X_0/X_1 and R_0/X_1 ratios such as exist at Grand Coulee whose present ratio of X_0/X_1 is approximately 0.5 and with an expected future ratio of 0.4 (without neutral reactors), it may be possible to further reduce the transformer insulation level with a very considerable cost saving on future Coulee transformers. These transformers would then be protected by correspondingly lower-rated lightning arresters.

If the solid-bus scheme is adopted, the present 10,000,000-kva circuit breakers now being installed may have to be changed to larger circuit breakers in the near future.

Consideration should be given to the possibility of reducing future transformer insulation with its resulting saving in cost and improvement in reactance. This will require a low X_0/X_1 ratio. With the use of neutral reactors as a method of limiting ground current to 10,000,000 kva, this ratio may be increased above the value required to permit reduced insulation level.

Use of the ring-bus reactor scheme with full neutral grounding will permit a substantial saving in circuit breaker costs, as smaller sizes are required will save the cost of neutral reactors, and permit a possible saving in future transformer cost by maintaining low X_0/X_1 ratios to permit a reduction of insulation level.

V. M. Marquis and H. P. St. Clair (American Gas and Electric Service Corporation, New York, N. Y.): The authors have presented a very clear picture of the problem with which the Bureau of Reclamation was faced in deciding upon the switching layout and bussing arrangement for the Grand Coulee power plant. The various considerations leading to the final decision for installing a ring bus without reactors have been nicely set forth and we believe that a correct analysis of these considerations has been made and that sound conclusions have been reached.

On the 138-kv American Gas and Electric Company system, we have always felt that our high-voltage switching stations should be solidly bussed, and this practice has been generally followed. The only deviation has been in those cases where circuit breakers of adequate interrupting capacity were not available at the time. For example, at our Philo Station it was necessary to split the high-voltage bus into two sections separated by a large 138-kv reactor in order to limit the circuit breaker duty to the maximum of 3,500,000 kva then available in circuit breakers. Interleaved line connections with a split bus arrangement have also been used on a 66-kv system for similar reasons. In both cases a relatively heavy synchronizing tie has been maintained so that stability has not been affected. These practices however, have been regarded as temporary, to be discontinued when the necessary higher capacity circuit breakers are available. Operating experience has confirmed the superior flexibility of the solid bus, and for the Philo Station, plans are definitely made for the replacement of existing circuit breakers and the removal of the bus tie reactors.

We note that it is proposed to install reactors in the neutral of the 230-kv step-up transformers in order to limit line-to-ground faults to a maximum equivalent of 10,000,000 kva. This is undoubtedly a sound procedure, since, as the authors have pointed out, line-to-ground faults will be far more frequent than 3-phase faults, particularly for locations close to the power plant. We were wondering, however, whether the equivalent limitation in line-to-ground faults could not have been obtained in a less expensive manner by incorporating in the transformer design a moderate amount of additional insulation to permit operating some of these transformers with the neutrals ungrounded, relying on immediately adjacent transformer banks to supply adequate system grounding. This in effect utilizes the impedance of some of the existing transformers to limit the ground current and is a method which we have used successfully in two of our major power plants at 132 kv. In these cases 46-kv insulation in the neutral was adequate to permit leaving some of the transformers ungrounded. Perhaps the authors did investigate this possibility and found it to be more costly than the reactor scheme.

It is also believed that the authors have reached a sound conclusion in depending upon 10,000,000-kva circuit breakers for the ultimate layout even though a maximum theoretical 3-phase fault on the station bus of 12,000,000 kva is possible. The analysis of probabilities, as given in Figures 7, 8, 9, and 10, showing the likelihood—or absence of likelihood—of getting more than 10,000,000 kva in an actual system fault, does not lead to too much concern over the 12,000,000 kva possibility, particularly since some additional margin of capacity would probably be available in the circuit breakers themselves. In this connection it might also be pointed out that the full offset current wave which apparently is necessary to realize 12,000,000 kva would not necessarily produce the maximum condition of severity as far as circuit breaker interrupting capacity is concerned. The reason for this is the fact that when the wave is fully offset it is impossible to obtain a high transient recovery voltage, or rate of recovery voltage rise, since under these conditions the current and voltage both pass through zero at about the same time. This, of course, leads to a question as to the validity of our entire American practice in rating circuit breakers on the total rms value of current interrupted, even to the extent of taking into account a completely offset wave. European practice, as we all know, does not credit the circuit breaker with interrupting anything more than the a-c component regardless of whether the current wave is offset or not. We understand that some consideration is being given in this country to the advisability of changing our own practice in this respect.

For a generating station of the magnitude of Grand Coulee with its network of 230-kv outgoing lines, there does not seem to be much question but that the 230-kv ring bus operated as a solid ring with a number of normally-closed sectionalizing circuit breakers is a sound solution. It also appears reasonable to consider transformer differential operation of sufficiently infrequent occurrence that a clearing of the entire bus section would be justified. This would appear to be a satisfactory starting point at least and could undoubtedly be modified if future experience warranted it.

The apparently successful development of 10,000,000-kva circuit breakers is, of course the thing that has made it possible at Grand Coulee to operate a solid 230-kv ring bus, and corresponding developments at other voltages will also make possible the use of solid high voltage busses generally. This is gratifying to those of us who have followed these problems through the years and who have participated in some degree in development of higher capacity circuit breakers through field testing programs.

W. M. Leeds (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The importance of circuit breaker interrupting capacity in the determination of a suitable bussing arrangement for Grand Coulee power plant is made very clear both in this paper and in the paper by Hoard and Helwith. The operating flexibility inherent in a sectionalized ring bus system with minimum reactance between sections can be realized only if circuit breakers are made available of approximately three times the interrupting capacity of existing equipment. Considerable credit is due to the Bureau of Reclamation engineers for their confidence that such circuit breakers can be built, and particularly for their co-operation in extensive short circuit testing to verify the interrupting ability of sample circuit breakers submitted by various switchgear manufacturers.

In considering the 10,000,000-kva 230-kv 3-cycle oil circuit breakers now on order for Grand Coulee, it should be pointed out that these are special circuit breakers which deviate in several respects from established standards. In the first place, a continuous current-carrying capacity of 1,000 amperes has been specified to take care of particular emergency conditions, this being intermediate between the 800 ampere standard rating for 230-kv circuit breakers and the next step of 1,200 amperes available in circuit breakers at lower voltages. In the second place, the interrupting rating of 10,000,000 kva has been specified on a 20-cycle reclosing duty cycle. Since a derating factor is applied to standard circuit breakers used on this severe duty, it might be inferred that a higher interrupting ability is available on a standardcloseopen—15 second—closeopen duty cycle. In this case, however, the exceptionally high momentary currents for

this class of equipment that would be involved, together with a reasonable conservatism about extrapolating too far beyond the limits of past experience have made it desirable to avoid giving any kind of rating above 10,000,000 kva.

It is stated in this paper that the maximum line-to-ground fault current with the neutrals of the transformers solidly grounded would be 30 per cent above the 3-phase value, whereas Hoard and Helwith mention an increase of only 15 per cent. Would not the large figure apply only to the contribution from the Grand Coulee generators to a solid bus fault, the factor for the combined short circuit power including the contribution from the transmission system being perhaps an intermediate value of about 20 per cent?

Although it has been decided to operate the pump motors and associated generators separate from the rest of the system, if all 18 generators and 10 synchronous pump motors were to be connected to the system at one time, what would the estimated maximum 3-phase fault be then?

E. L. Kanouse (Department of Water and Power, Los Angeles, Cal.): This paper by Holmdahl and Killgore will be of great interest to all utility engineers. The locale of their problem is in itself of considerable interest, the largest hydroelectric power plant in the world, and the paper provides rather complete insight into the engineering analysis employed, with the results and conclusions following with complete logic.

Certainly the solid connection described as the solution to the bussing problem meets the requirements laid down by the authors. It is distinctly encouraging to know that the extremely high interrupting capacities required of the circuit breakers can in all probability be met by modern design.

However, there are many utilities for which the solid bussing arrangement to be employed at Grand Coulee would fail to meet a requirement not mentioned. This requirement would be that the short-circuit duties on lower voltage, subtransmission, and distribution systems not exceed certain nominal values.

In system planning studies recently conducted by the Los Angeles Department of Water and Power, it was found that power concentrations, within the city limits representing the combined contributions from both distant hydroelectric and nearby steam electric plants, would soon result in short-circuit duties in excess of 5,000,000 kva on the 138-kv backbone of the system. And while it was recognized that circuit breakers could probably be designed for this interrupting capacity, it was apparent that the existence of such high duties at the 138-kv level in the system would produce excessive duties on switches at voltages below this level in the distribution system. Extensive replacement of switches at 34.5 kv and below would be the inevitable result.

Accordingly, the city found it desirable to utilize a method of connections employing 138-kv air core reactors by means of which 138-kv circuit breaker interrupting duties may be limited to approximately 3,500,000 kva for an indefinitely expanding system. This will extend the useful life of hundreds of 34.5-kv switches in the distribution system.

Each of the city's six large receiving stations has been designed and operated to date as a double bus, double circuit breaker station. Current limiting reactors have long been contemplated in the busses, for separating the power source and load side of the station from the so-called belt line side. The belt lines, or interconnecting lines between stations, are designed as standby lines for carrying the load of the station in case the normal power supply is either interrupted or inadequate for any reason. Ten per cent reactors, on 100,000-kva base, have been purchased for the various stations, although studies showed that as the power system expands and additional receiving stations and generators are added, these reactors would fail to limit the short-circuit duty to the desired 3,500,000-kva level on the belt line side.

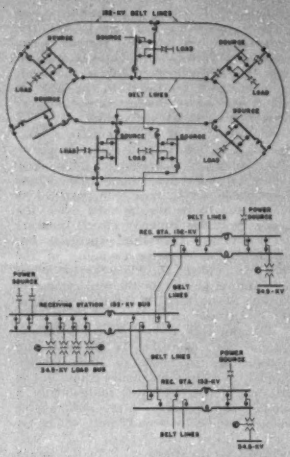

Figure 1. Double-bus single circuit-breaker belt line operation

The difficulty obviously arises because the use of double circuit breakers results in paralleling the two 10 per cent reactors in the busses, thereby halving their transfer impedance at each station. It was found that by utilizing double-bus single circuit breaker operation on the belt line side of all receiving stations, remarkable reductions in the short circuit duty of the belt line switches result. Furthermore, the studies showed that the power system can be expanded, by the addition of receiving stations either tapped or looped into the belt lines, to care for future load demands of at least 2,200,000 kw, and probably considerably more, without exceeding the interrupting capacity of many existing 138-kv circuit breakers.

The fact that this single circuit breaker method of operation reduces by half the number of 138-kv switches needed in the belt line positions is also quite attractive. However, the chief aim in developing the general system plan using current limiting reactors and belt line ties has been to hold the short-circuit duties down on the lower voltage subtransmission and distribution systems, and thereby avoid future extensive replacement.

It would be of interest to know the possible ultimate switch duties at the various points of bulk power delivery on the Bonneville system, such as at the 230-kv Columbia bus.

N. G. Holmdahl and C. L. Killgore: The various discussions of the paper have accentuated various sections of the paper to a marked degree as well as being personally gratifying to the authors. These discussions add greatly to the technical aspects of the subject.

Each of the discussions are treated individually and answers are given to the questions contained therein.

Lantz is concerned with two aspects of the problem of providing circuit breakers for the ultimate Grand Coulee power plant, (1) that 10,000,000-kva interrupting capacity circuit breakers will be too small with maximum generation both for a 3-phase fault and even more so for a single-line-to-ground fault without transformer neutral reactors and (2) that if too large a neutral reactor is used, the X_0/X_1 ratio will exceed three and thereby lose the benefit of reduced transformer insulation costs. He proposes that 230-kv bus reactors be installed to correct these conditions.

As stated in the paper, cognizance is taken of the fact that without using 230-kv bus reactors and for the worst possible conditions of fault, that is, maximum generation connected, full offset of the current wave and the fault occurring on a line immediately adjacent to the switchyard, that 3-phase faults may exceed the circuit breaker rating.

However experience has indicated the probability of the occurrence of such a combination of conditions simultaneously is exceedingly remote and that economically the additional capital investment for 230-kv bus reactors is not justified.

Plans are being made to install reactors in the transformer neutrals of such a value that the single-line-to-ground fault kilovolt-amperes will be equal to or less than the 3-phase fault megavolt-amperes. Such reactors will not increase the ratio of X_0/X_1, over three and will allow reduced transformer insulation with its reduced cost. Transformers now being purchased for the right power plant will have 196-kv insulation level.

From the foregoing, it is believed that Lantz' suggestions for reduced transformer costs will be realized without the additional cost of 230-kv reactors.

Smith suggests that a change be made in the bussing at the 230-kv switchyards such that no pair of lines to any intermediate substation would be terminated on the same bus section at Coulee. This point was considered in the preliminary discussions between the Bonneville Power Administration and Reclamation engineers but it was finally agreed to leave both lines, that is Coulee-Midway number 1 and Coulee-Midway number 2, connected to the same bus section for the following reasons:

1. All existing lines terminated in pairs at one bus section and rerouting would be costly and would add to the complexity of the switching stations.

2. With the future development of Columbia River, the 230-kv network would undoubtedly

become more solidly bussed with more high capacity ties, thereby minimizing the effect of the loss of two lines to any substation.

Crary's commendation of our paper is appreciated. In his discussion he points out that the recent circuit breaker developments leading to the development of the Coulee ring bus have come at an opportune time when many other American and European systems are planning to go to higher transmission voltages. It is by this mutual exchange of ideas and experience that the industry is able to meet the needs of the greatly expanding requirements of our civilization.

Dovjikov brings out a point which most of us are prone to overlook, that is that even though the designs and plans are based on the best knowledge available and the best interpretation of past operating experience, still the ultimate success of the Coulee ring bus will be determined by its actual performance in operation. The physical layout is arranged so that 230-kv bus reactors may be installed in the future if operating experience proves this to be desirable.

The authors appreciate Marquis and St. Clair's comments that in their opinion a correct analysis was made and sound conclusions reached, and for their discussion of the American Gas and Electric Company's experience with solidly bussed switchyards. From their point of view, a solid bus should be installed at all stations were it not for circuit breaker limitations which existed in the past. Their experience points to the desirability of omitting all bus reactors in order to simplify the operating procedures and remove any bottlenecks which reactors introduce. Such a statement reassures the engineers involved in the design of the Coulee bus, that their conclusions have basis in past experiences on other electric power systems.

The discussors suggest that operation of a portion of the transformers with neutrals ungrounded be considered rather than installing neutral reactors to reduce single-line-to-ground faults to the 3-phase value. Consideration was given to such a scheme but it was finally rejected as the costs in both cases were very nearly equal and it was felt that all units connected identically, that is with neutral reactors, was more desirable than having part of the units grounded and part ungrounded.

Leeds has amplified the special characteristics of the 10,000,000-kva circuit breakers now being manufactured for Coulee and in addition (a) questioned the value of 30 per cent for the increase of single-line-to-ground faults over 3-phase faults and (b) requested the value of 3-phase fault kilowatt-amperes if all 18 generators and 10 pumps were connected simultaneously.

In answer to the first point, Leeds is correct in his assumption that considering both line and generator contribution to a fault, that single-line-to-ground faults exceed 3-phase faults by only 15 per cent.

Present calculations of maximum fault megavolt-amperes are based on the contribution from 17 generators (one generator on 115-kv system) and nine transmission lines (assuming fault on tenth line just outside Coulee bus). The maximum calculated 3-phase fault on this system is 10,500,000 kva while with ten pumps in addition the fault is increased by about 700,000 kva.

Kanouse's discussion comments on the satisfactory use of 138-kv reactors in the Los Angeles Bureau of Power and Light system and requests information as to the effect solid bussing at Coulee will have upon circuit breaker duties at adjacent substations such as Columbia.

Considerable analysis of this problem has been done by the Bonneville Power Administration engineers on their network analyzer and results have indicated that at some points, circuit breaker duties will exceed the ratings of presently installed circuit breakers. In the conversion from the interleaved scheme to the ring bus, it was necessary to take an intermediate step before delivery of the 10,000,000-kva circuit breakers could be made. This step consisted of rebuilding six of the 8-cycle 2,500,000-kva circuit breakers for a 7,500,000-kva interrupting capacity for temporary use in line positions. These six circuit breakers will later be used by the Bonneville Power Administration after the ring bus is in operation for installation at such stations as Columbia and Midway where circuit breaker duties will be increased. It is believed that the balance of the Bonneville Power Administration stations will not be affected enough to make present circuit breakers obsolete.

The authors welcomed the several interesting discussions on the paper together with their comments and suggestions. At present, the left switchyard has almost been changed over to solid bus operation using 7,500,000-kva circuit breakers in the line positions. The changeover in itself has presented many interesting operating problems in maintaining full output of the left power plant at about 1,100,000 kw while making numerous circuit breaker moves in the left switchyard. However, as stated before, the left switchyard conversion is almost complete and operation with a solid bus will begin in December of 1948, with full ring-bus operation scheduled for the spring of 1949.

A New Outdoor Air Switch and a New Concept of Contact Performance

S. C. KILLIAN
MEMBER AIEE

The first electric switch used was doubtlessly a model of simplicity. It was likely made of two metallic wires or strips twisted together and when an open circuit was desired the two pieces were simply pulled apart. Something similar to the common knife switch was the next step and then as the industry grew, interrupting devices plus a host of special switches designed to do a particular job made their appearance. When electric equipment began to be used outdoors, another type of switch was born, the outdoor air switch. Due to adverse outdoor operating conditions such as corrosion and ice, it was quickly realized that some auxiliary blade motion was necessary if extreme operating forces were to be avoided. In the past 35 years, this auxiliary motion has led to a veritable maze of designs. The closest approach to perfection is in the gang-operated vertical break switch built on three insulators. Almost endless variety is obtainable in this one class alone. It is with this type that this paper deals.

In order to secure some auxiliary ice breaking motion and then raise the blade, many ingenious mechanisms have been used, most of which employ from 10 to 15 small moving parts. This complicity of bearings, cams, levers, links, gears, cranks, and knuckles inherently makes for trouble. The new switch, type *MK-40*, is the result of a search for simplicity and a desire to get back as close as possible to fundamentals with a minimum number of parts while retaining all the advantages of 30 odd years experience in switch design.

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S. C. Killian is chief engineer with the Delta-Star Electric Company, Chicago, Ill.

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Mechanical Design

Only three moving parts beside the blade are employed. These are shown in Figure 1A. Part *a*, bolted directly to the rotating insulator cap carries a double set of ball bearings at a 45 degree angle and drives the 45 degree knuckle pin, part *b*. This in turn rotates the blade through carriage *c* using another double set of ball bearings. The blade is bolted solidly in part *b* and completes the moving members. The blade makes point contact at both its ends with shoes *d*. No braids or flexible shunts are required. With the switch closed, the first 15-degree rotation of the insulator rotates the blade about its own axis, relieving contact pressure and exerting a large ice and corrosion breaking force. The next 23-degree insulator rotation produces a blade rise of only 10 degrees, providing in this ratio a sizable mechanical advantage exactly where it is needed. Figure 1B shows the mechanism parts with the blade partly open. For the final part of the blade lift where no mechanical advantage is required, the blade rise in degrees exceeds the insulator rotation in degrees. Figure 1C shows the switch fully open. The identical sequence in reverse occurs as the switch closes. The simplicity of design and motion is readily apparent. It is interesting to note that the blade is locked in both open and closed positions by having the knuckle pin move over dead center in both positions. The blade is under positive control during the complete cycle, requiring no stops or auxiliary devices to impart the turning and lifting motions. Figure 1D gives a close-up of the contact and bearing construction at the hinge.

Small members are studiously avoided in the entire design. After years of service, corrosion can cause small members to weaken or fail. Spring members especially are to be kept as large and heavy as possible. A three-leafed spring of 3/16 inch beryllium copper (9/16 inch total thickness) is used on each side of each contact on this switch. A large spring deflection, 3/16 inch total, is used so that wear on the contacts and blade over a period of years will not materially effect the contact pressure. Bearings are made large which reduces ball bearing pressures. Moving parts are exceptionally husky.

Contrary to some contact design thinking, the spring member and the hard drawn copper current carrying member are independent. No alloy can be compared to copper for electrical conductivity. When independent of the electric circuit, the best spring available can be used. When the contact and spring members are combined, some of the ideal characteristics of each must necessarily be sacrificed.

For voltages above 69 kv the weight of the blade is effectively counterbalanced by the use of enclosed torsion springs mounted directly at the hinge pins of each unit. The switch is constructed in 400, 600, and 1,200-ampere capacity from 7.5 kv to 230 kv. Higher capacities are built with square tubing blades in a somewhat similar design. These have been in service since 1941. A 4,000-ampere 23-kv unit is shown in Figure 2. Counterbalance spring housings are shown at A.

Electrical Design

Because an air switch must operate in all kinds of weather and in contaminated atmospheres, trouble experienced has been more of a mechanical nature than electrical. This has led to emphasis being placed on mechanical features and design rather than electrical. Though the elec-

Figure 1. The new switch, 600 ampere capacity, showing mechanism and contact details

trical design is less complicated than the mechanical, it is of primary importance and will become more and more important as the enlarged capacities of many systems will put ever increasing short-circuit loadings on such equipment. In addition to carrying its full load under a given temperature rise, and employing contacts which will give satisfactory service under bad atmospheric conditions, the design must be such that short-circuit currents will be handled without distress. A high pressure, small area contact such as used on all switches of this type is basically a spot welder. The only reason contacts do not weld together under high fault currents is the ability of the contact to carry the generated heat away quickly, and also provide a volume in which to store this heat. By using hard drawn copper parts for all current interchange members, advantage is taken not only of 100 per cent electrical conductivity but of a very high thermal conductivity. The law of Wiedemann and Franz states that for metals at a given temperature

$$\frac{\text{Thermal conductivity}}{\text{Electrical conductivity} \times \text{absolute temperature}} = \text{Constant} \qquad (1)$$

In this simplified formula, if the thermal conductivity is given in joules per centimeter per second per degree centigrade, the electrical conductivity as the reciprocal of the resistivity in ohm-cm $\times 10^6$ and the temperature as 0 degree centigrade, the constant according to Cork[1] is approximately 2.3 (10^{-6}) in joule ohms per second per degree centigrade squared. All metals, by taking the ratio of their two conductivities, produce this same value, approximately 2.3. The writer has calculated this ratio for alloys and finds that the law holds just as rigidly as for pure metals.

It follows therefore that a high electrical conductivity and high thermal conductivity go hand in hand. Copper has a thermal conductivity of 0.92 gram-calories per square centimeter per degree centigrade per centimeter at 20 degrees centigrade, as compared to approximately 0.30 for various copper alloys and bronzes. According to Moore[2] the thermal capacity for unit volume for materials which would usually be used in contacts varies over a range of 40 to 56 watt seconds per cubic inch per degree centigrade. The value of 56 is given for copper. The volume of the contact is of even greater importance for it is this mass which must act as a heat sink when a short circuit occurs. The copper contacts on this switch are 1/4 inch thick and are very ample for rated short-circuit duty. Double shoes are used at both ends of the 1,200 ampere design as shown in Figure 3 to afford four points per contact. Copper blade walls vary with both current and voltage rating and are in keeping with contact sizes. As the blade itself makes contact with the shoes, the number of current interchange surfaces as well as the number of moving parts are held to a minimum. Temperature rises at rated currents are well below the allowable 30-degree centigrade value.

Contact History

This switch carried a high pressure small area type contact at both ends of the blade. It is extremely interesting to search back into the history of this contact which is now almost universally used on air switches. Some of the historical highlights follow. In 1906, Browning[3] realized the importance of pressure as related to contact resistance but probably due to the large areas he used, 1.54 square inches, found a point at 30 to 35 pounds per square inch beyond which an increase of pressure produced no additional drop in resistivity. In 1912, Binder[4] studied contact shapes and conducted comprehensive tests in which he attempted to show that contact drop was a function of pressure alone. In 1913, apparently quite independently, Harris[5] published four laws of contacts, the fourth of which read as follows: "The resistance between contacts depends not upon their area but only on the total pressure with which they are forced together."

Figure 2. 4,000-ampere switch

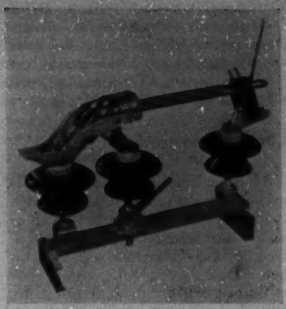

Figure 3. The new switch, 1,200 ampere capacity

A considerable quantity of material on this subject appears in the literature following the above dates, probably the most notable of which is that by Schaelchlin[6] and Contius.[7] In the generalized formula

$$r = \frac{k}{p^n} \qquad (2)$$

where

r = contact resistance
k = constant depending upon contact shape
p = pressure in pounds
n = constant depending upon contact surface

Contius (from Windred[8]) evaluates comparative contacts. The constant n varies from 0.5 for points and crossed cylinders to 2 for small plane surfaces of 2 and 3 square-centimeter area while k shows a minimum of 0.00015 for crossed cylinders and 0.00020 for points as compared to a maximum of 0.4 for ground planes 3 square centimeter area.

Of more recent date is a book by Windred[8] and of very recent date an

exceptionally thorough book by Holm[9] both of which deal with contact theory. For a general bibliography on the entire subject the reader is referred to the American Society for Testing Materials Contact Bibliography.[10]

Contact Design

Though a large amount of literature is available on the subject of high pressure contacts, no one has apparently tried to explain their superiority to large area contacts by means of diagrams such as shown here. Figure 4A shows a contact shoe of the new switch with apparent contact area shown shaded. Since the shoe is formed on a radius and the tubular blade is formed with rounded edges, the contact can be compared to two cylinders crosswise. Figure 4B illustrates a large-area medium-pressure contact. Authorities now definitely agree that it is impossible to grind two contact surfaces flat so that they touch at all points. The large-area contact of apparent area *lw*, will touch at only three points as shown by shading, Figure 4B. The area of these points will be determined by the pressure on the contact as well as by the hardness of the contact material. If the material is soft, these high spots will flatten and possibly new contact points will form. Of the shaded areas only those spots shown in black will conduct current. These spots are called A-spots by Holm.[9] The remainder of the shaded area, due to always present films such as oxides and sulphides, does not make a good conductor. On the other hand in Figure 4A, the apparent contact area is approximately equal to the shaded area or actual surface contact area. The total area of the A-spots is larger than those in Figure 4B because the higher pressure used in Figure 4A allows for more shaded area, more intimate contact, more film breakdown due to pressure means and consequently larger A-spots. This discussion refers to nominally clean contacts. Heavily filmed contacts are discussed in the following section.

It so happens that for the ordinary large-area contacts the blade must be withdrawn as in a knife switch. High contact pressures cannot be used, as the operating effort becomes excessively high. With the point or line contact, however, pressure can usually be released before moving the switch blade away from the contact and this results in easy operation. If sufficiently high pressures could be used on large-area contacts, there is no reason why they should not function equally as well as contacts of smaller area. This very likely explains the perfect record of hinge contacts on hook stick switches. Here, in most designs, pressures of the order of 400 or 500 pounds, provide a very satisfactory contact, even though "apparent" contact areas are large.

Contacts at both the break end and hinge end are made up of the hard drawn copper blade operating against rolled silver inlays on hard drawn copper shoes. This combination of metals was picked after a long program of contact abrasion testing on many materials because it provided a contact with abrasion-free characteristics. Contact pressure is approximately 200 pounds. An approach to a point contact is used rather than a line contact because of ease of adjustment. One can see readily that even though the shoes or blade be slightly misaligned the contact between them (2 crosswise cylinders) will not be affected. If a line were used on either of these members against a surface, a slight misalignment would make it very difficult to get the line to lie flat on its engaging surface. A point contact would therefore result.

The Effect of Films on Contact Performance

Contact films must be given more consideration on outdoor air switches than on any other type of equipment. All air switch contact literature and technology to date have dealt with only clean contacts. However, usually by the time the switches are installed they already have accumulated some film, and as they are in use for practically their entire life in a filmed condition, this feature requires definite consideration. As these contacts are entirely exposed to corrosive atmospheres, film growth occurs very readily and these films play an important part in reducing the contact's efficiency. The resistance of the various films which ordinarily form on air switch contacts in use today are extremely high. Silver oxide and silver sulphide, though of very high resistivity at room temperature, are decomposed comparatively readily under heat. The oxides and sulphides of copper on the other hand do not break down readily under heat.

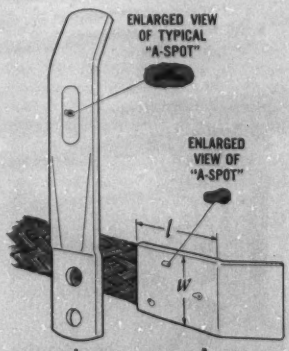

Figure 4. Comparison of contact area of high-pressure small-area contact and medium-pressure large-area contact

Other phenomena help puncture these films, however, and it is likely due to these that very badly corroded contacts function at all when closed after standing open for long periods of time. Possibly the most important phenomenon in this respect is "coherer action" (see Holm[9]). This action is thought to take place as follows: If the film is of high resistance, free ions will migrate through the film, after which fluid metal will form exceedingly fine bridges across the film. (Estimated diameter = 400 Angstroms[9]; 1 Angstrom = 10^{-8} centimeters). These bridges form at a critical voltage, called the "coherer voltage" which is related to the melting point of the particular contact metal as well as to the thickness of the film. In testing for this critical voltage, large initial contact resistances are read. Voltage is gradually increased until some voltage is reached where the resistance suddenly falls to a very small fraction of its original value. This coherer voltage is usually of the order of a few volts or less on thin films, though Holm did require as much as 100 volts to break down a nickel oxide film 1,250 Angstroms thick. This phenomenon is not dependent on any combination of metals or circumstances, but occurs on all metals and through all types of film. Coherer action through varnish films has been shown and Pearson[11] has proved that this action occurs on contacts actually separated by small distances, in which case the film is air.

As far as can be found by a careful search of the literature, few tests have been made on filmed contacts on an actual switch. Also, the literature, including Holm's very complete work, deals with film breakdown largely from a voltage standpoint. For pure physics this makes an ideal method. The usual method, however, of checking contact efficiency on an air switch is to take a millivolt drop across the contact with some constant voltage on the whole set-up. This drop then becomes a function of current and with this in mind it was decided to investigate films from a current standpoint.

This is closely related to voltage breakdown for when current is increased over a contact, a larger voltage will appear over the contact for an instant. While not as exact as the voltage method, it is more practical on switches of this kind. Air switch contacts usually have a plentiful source of voltage available to break down contact film, for in actual operation of a system, thousands of volts may appear across the contact if its resistance is very large. More research on voltage breakdown remains to be done, for film studies so far published have not dealt with films nearly as thick as those which accumulate on outdoor switches.

This method of testing also brings up the question of whether tests should be made on corroded contacts at some small current or at currents close to the rating of the device. Almost any current might be used in order to get a drop across the contact, varying from a fraction of an ampere to hundreds or thousands of amperes. If films are broken down by currents close to the contact's rating, testing with small currents will produce erroneous conclusions.

For the purpose of checking films on contacts composed of different materials, a contact such as that on the break end of the new switch was chosen. Four contact combinations were tested as follows: rolled copper shoes and bronze blade (a tin, lead, zinc, bronze of 20 per cent electrical conductivity); rolled copper shoes with silver inlays and a silver plated copper blade; rolled copper shoes with silver inlays and copper blade; rolled copper shoes and copper blade. The copper blade in all cases was hard drawn copper tubing. It was felt that these four combinations would provide information on most contacts in use today. Pressures of approximately 200 pounds per point were used. Drops were taken from point x to point y, Figure 1A, which included the entire length of the shoes and part of the adapter casting. The resistance from x to y with clean contacts was approximately 35 microhms for all four contact combinations. All contacts were 600-ampere capacity. Direct current was used in making measurements. After measurements were taken, a-c heat runs were made. These showed that the passage of alternating current through the contact did not change the contact's resistance if the alternating current was of the same order of magnitude as the direct current.

Any film deposited on a contact is relative since there is no standard method of procuring a dirty contact or an oxidized sulphided or corroded contact. Films

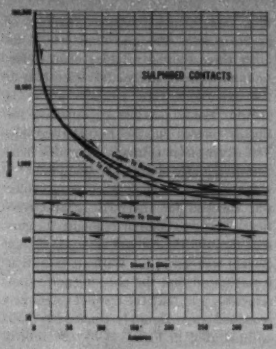

Figure 5. Resistance characteristics of sulphided contacts

are deposited on outdoor switch contacts by corrosive atmospheres. The action of these atmospheres can be simulated by putting the contacts in a chamber which is then filled with active gases. This procedure is slow however for repetitive testing, and accordingly was not used. The first set of tests was made on contacts sulphided with a solution containing one part by weight of potassium sulphide to nine parts water. This solution was applied to the blade and contact of the open switch with a glass rod, so as to completely wet the contact surfaces. The solution was left to dry overnight. A dense, strongly adhesive, black sulfide film resulted on copper, bronze, and silver surfaces. No method was available for measuring the film thickness but it was of such order that any maintenance man would recommend cleaning these contacts if he had them on his system. Great care was taken to apply the corroding agent in an identical manner to each contact and to each set of tests. That this was accomplished was shown by the closeness with which most curves followed preceding curves on the same contact. The switch was closed (only one closure made) and readings were taken.

Test results were most interesting and are shown in Figure 5. At currents of about 1/2 ampere, resistivities of several hundred thousand microhms were found on both copper-copper and copper-bronze. If smaller currents were used, it is very likely that even higher resistances would have been found. As the current was increased, the resistance fell continuously until it was only 300 to 500 microhms at 400 amperes. The sulphides and oxides of copper are extremely adherent and it is not likely that any switch contact can wipe them away. This has been forceably demonstrated on connectors with toothed surfaces, clamped on oxidized copper wire. Even though thousands of pounds of pressure were available by means of clamping bolts, the oxide film was not cleaned away. This explains the need of such a phenomenon as coherer action in order to make the copper-copper and copper-bronze contacts suitable conductors.

A most revealing phenomenon occurred on these two contacts. It was found that after a current of a few amperes was exceeded, the voltage drop across the contact was almost constant, up to several hundred amperes. This, of course, meant that as the current went up, the resistance came down proportionately. On the basis of Holm's theory that small filaments of molten metal were formed through the film, we could deduce that each rise in current caused these filaments to grow in exact proportion to the current.

A digression seems appropriate here in analyzing short-circuit currents on heavily filmed contacts. If the connecting filament does grow as suggested before, it will attempt to grow very rapidly when the current suddenly increases from a few hundred amperes to many thousand amperes under short circuit, and the accompanying evolution of heat and gases will probably blow the contact apart momentarily if the parts are not held together with sufficient pressure. This will cause arcing, sputtering, and burning and may explain the occasional burning on corroded medium pressure, large area contacts. The scope of testing in this project was limited to only load currents. Further testing on the behavior of various filmed contacts under short-circuit conditions is planned for the near future.

Unlike the copper-copper and copper-bronze contacts, the copper-silver normally showed a much lower resistance. Resistances of approximately 200 microhms appeared at small currents. This decreased gradually at increased currents until values of approximately 100 microhms were reached at 400 amperes. In two cases out of six tested, this combination showed a characteristic similar to the copper-copper characteristic, but was much lower in initial and final resistances. This cannot be explained readily, but some explanation was suggested in the contact condition. This will be discussed later.

The silver-silver contact performed very well, producing resistances of approximately 40 microhms at all currents. Three possible explanations for these

low readings offer themselves: (1) film disintegration due to heat from the passage of current; (2) Holm's filament formed through the sulphide in such large diameters, due to the ready breakdown of the film, that low resistance immediately appears; (3) the sulphides of silver are not as strongly adherent as the sulphides of copper and are wiped away when the switch closes. Probably (1) and (3) together make this combination a particularly efficient one. (2) is not likely. These remarks also apply to the copper-silver contact in a lesser degree.

Tests on oxidized contacts were next made. Results are shown in Figure 6. The oxidizing agent was applied in the same manner as the sulphiding medium. A commercial oxidizing agent was used which produced a heavy coating of cupric oxide and silver oxide. Results followed those of sulphided contacts quite closely, though values as high as 775,000 microhms and 575,000 microhms were recorded on copper-bronze at low currents. Copper-silver showed constant average values around 100 microhms in five of six tests. The sixth test showed a characteristic similar to copper-copper but much lower in initial and final resistance. Silver-silver repeated sulphided values at a constant of approximately 40 microhms. The same line of reasoning as applied to sulphided contact performance, holds for oxided contacts.

Tests on contacts coated with a solution composed of equal parts of the sulphiding and oxidizing solution were next performed. Results are shown in Figure 7. The characteristic pattern again appeared, copper-bronze producing a high value of 839,000 microhms. Of six copper-silver tests performed with this film, three showed constant readings of approximately 100 microhms while the other three showed a falling characteristic from approximately 1,000 microhms to 250 microhms. An average curve is shown. Some possible explanation for this is offered in the next paragraph. The silver-silver contacts showed a constant resistance of about 40 microhms.

On all tests made, contact performance was checked for reversible characteristics. This was done by reducing the current through the contact after maximum current was reached. Without exception it was found that the phenomenon is not reversible and that the contact resistance remained approximately constant. This is illustrated in Figure 5 by the return curve indicated by fine lines and arrows. Contact resistance could be raised very materially by striking the channel base of the switch repeatedly with a mallet. This apparently broke or damaged the filament through the film.

Another contact phenomena was studied when several sets of contacts were highly burnished before being coated. This was done by operating the switch many times until a very high polish was obtained on the contact surface. Just as plating on a highly polished surface produces a highly polished plate, the half oxide, half sulphide solution produced a polished film on this surface. Of three silver-copper contacts tested, all showed the characteristic decreasing resistance pattern. Highest readings obtained at 1/2 ampere were 90,000 microhms which fell to less than 300 microhms at 350 amperes. Even the silver-silver combination showed relatively increased resistance under this polished condition, the one test performed producing 960 microhms at 1/2 ampere which fell to 160 at 350 amperes.

The literature produces verification of this. Schaelchlin[6] found that on contacts exposed to the air for one week at 30 degrees centigrade, a contact roughened with emery cloth produced lower resistances than what a carefully ground contact did.

In formula 2 Contius[7] includes a value n for surface condition. He further states that "particularly smooth surfaces are characterized by high resistances."

In all tests with the exception of the four previously mentioned, contact surfaces were prepared by cleaning with medium fine steel wool before applying the film. In these polished cases not only was the action of the polished contact being effected, but due to the smooth surface the closure of the switch failed to clean away any considerable film. This particular test was not tried on the copper-copper or copper-bronze combinations as they already showed very high initial resistance values. On the silver-copper contacts tested with no burnishing it must be supposed that the silver film helped in some measure to destroy the copper film mechanically as a general rule. When higher initial resistance values appeared on this combination it is likely that the film was not broken down mechanically. Burnished contacts such as these are not a possibility in the field, of course, because many operations will not be put on the switch in an hour or so and corrosion formed will prevent such a polish appearing when many operations are put on the contacts over a period of months.

One additional study was made on non-polished contacts filmed with the sulphide and oxide solution. The contact shoes were held apart until the blade was in place and then released, so as to avoid any wiping action. On three tests made with copper-silver contacts, high values of 160,000, 29,000 and 288,000 microhms were found at 1/2 ampere. These fell to the usual low values at 350 amperes. On one silver-silver contact tested a constant resistance of about 50 microhms appeared.

This proves further that the silver film must help break down the copper film mechanically when the switch is closed in the usual manner.

All resistance curves seemed to become almost horizontal at the limits of currents tested, 350 to 400 amperes. In order to check this, tests were continued on several runs to currents as high as 900 amperes. The general shape of the curve followed predicted values very closely. At 900

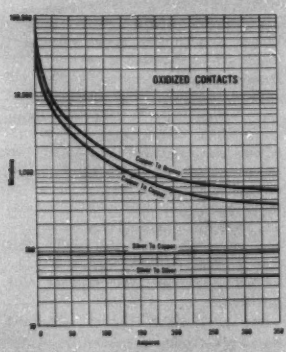

Figure 6. Resistance characteristics of oxidized contacts

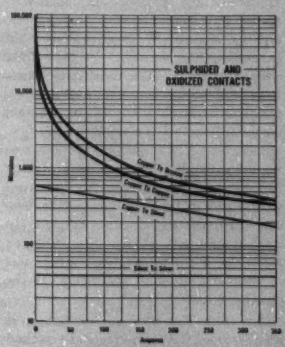

Figure 7. Resistance characteristics of sulphided and oxidized contacts

amperes, resistance values of 200 to 250 microhms were found where values of 400 microhms existed at 350 amperes, showing that in the higher current ranges, resistance decreases very slowly with an increase in current.

A total of 66 tests were made on these various filmed contacts and it is thought that they should be of sufficient quantity to fully support the conclusions reached here.

Conclusion

The new switch combines the following necessary engineering features: minimum number of moving parts, substantial construction, minimum number of current interchange surfaces, proper contact design and all around simplicity. Though silver-silver contacts proved best on film tests, their abrading characteristics under high pressure prevent their use. Copper-silver makes an ideal antiabrasive combination and provides a superior electric contact when compared to copper or the copper alloys. Additional work remains to be done on testing filmed contacts and it is likely that this particular contact condition will receive more and more attention.

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Discussion

J. B. Owens (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The paper describing the new outdoor air switch reveals that a simple design with a minimum number of moving parts has been achieved. However, some features which experience has shown to be desirable in outdoor switches have not been included.

The jaws on the hinge end of this switch form a pocket where snow and ice will collect, making the switch more difficult to operate under adverse weather conditions. Unfortunately it is not possible to enclose the jaws on both ends of an air switch; but it is desirable to cover the contacts on the hinge end. The addition of a cover over the hinge jaws would keep off ice, and in addition would keep the contacts comparatively clean thus reducing the maintenance required.

The blade actuating mechanism of the new switch appears simple and sturdy; however, a possibility for improvement exists in the design of the bearings above the rotating insulation column. The author states that large diameter ball bearings are used to reduce the ball pressures. These may be called upon to stand in the weather for long periods without operating or lubrication. If ice or corrosion fouls such large bearings they will be much more difficult to break free and operate than smaller diameter bearings because there is more surface to become fouled, and bearing friction operates on a greater radius so that a poorer mechanical advantage is available to overcome it. Small diameter bearings with materials which are suitable for the increased bearing pressures are desirable in outdoor switches.

The results of load current tests described in the paper prove the ability of the contacts to carry rated current. However, the paper states that short-circuit current tests were not made. Tests in our laboratories at currents up to 250,000 amperes for five seconds have positively shown that a line contact will carry more current without damage than a comparable point contact. The author points out that it is the metal immediately adjacent to the contact which determines the rate at which heat is conducted away and thereby determines the short-circuit current which can be carried without damages. A line contact inherently has a much greater amount of metal immediately adjacent to the contact and thus greater short circuit capacity.

The results of the test on electric contacts with oxide and sulphide films are of considerable interest. The author is to be congratulated on the application of classical contact theory to the explanation of air switch contact performance.

J. A. Adams (Philadelphia Electric Company, Philadelphia, Pa.): During the last few years there have been a number of new disconnecting switch developments, one of which was described previously in an AIEE paper.[1] The present paper is another contribution to this line of development. It shows that considerable thought has been given to both mechanical and electrical problems which affect the operation of these switches in actual service.

From an operating company viewpoint a switch should:

1. Be mechanically strong.
2. Be durable.
3. Be easy to install.
4. Be easy to operate.
5. Require little maintenance.
6. Have good current carrying capacity.

It would be hard to say which of these points is the most important; certainly, all of them have to be taken into consideration in the development in order to produce a well balanced design which will continue to give reliable service.

It goes without saying that the switch must be strong enough mechanically to withstand the operating forces and that it must do this under adverse conditions, such as when coated with ice, as well as withstand the stresses created by heavy short circuit currents. Also, all parts of the switch should be sufficiently durable to stand up under years of severe operating conditions. Delicate parts and adjustments have no place in this type of equipment. The switch should be designed so that it can be installed with a minimum of adjustments and they should not be critical. The necessity for ease of operation is obvious. From an operating viewpoint, low maintenance is one of the more important factors. This requires a design with as few moving parts as possible, and with few if any members which can get out of adjustment during operation. Elimination of braids is a step in the right direction. Not only must the switch be able to carry its normal current rating without excessive heating and with low contact drop and be able to carry short circuit currents without pitting of contacts, but it should also have the ability to carry heavy overload currents for short times without difficulty.

The foregoing factors should be taken into account not only in the design of the main switch but should also be incorporated into the design of grounding blades. While these blades may not be used frequently, they are just as important as the main blades when they are required. Another point of importance, not covered, is the design of the operating mechanism, which should take into consideration the mechanical factors discussed.

In the paper it is shown that although the contact resistance for copper-to-bronze and copper-to-copper contacts with films is initially high for small currents, it drops to relatively low values with increasing current where it remains even when the current is again reduced. In this connection, I have the following questions: For sulphided or oxidized contacts is it necessary to raise the current to high values before the lowest value of contact resistance will be obtained or will the contact resistance drop with continued operation at low current? For example, referring to Figure 5 of the paper, if the current is initially of the order of 75 amperes and remains at this value for a relatively long time, will the copper-to-bronze contact resistance remain at approximately 1,090 microhms, or will it eventually drop to the value of about 320 microhms as indicated for the final resistance? Does the operating voltage affect these values?

Low voltage tests made by the Philadelphia Electric Company on some new 88-S

Figure 1. Close-up view showing contact end of switch with blade in fully-open position

Figure 2. Close-up view showing blade in partly-closed position

kv, 1,200-ampere switches which had become tarnished while they were stored outdoors showed high contact resistance and high temperature rise at rated current. The temperature rise of the clip end contacts which were silver-to-copper was from 2 to 16 degrees centigrade above the standard AIEE rise of 30 degrees centigrade, while that of the hinge-end contacts which were copper-to-bronze was 36 to 50 degrees centigrade above the standard value. When the contacts were cleaned the temperature rises dropped to normal values. These tests indicate that careful attention should be paid to contact design and that contacts not seriously affected by films are to be preferred.

While the author indicates that this switch is constructed up to 1,200 ampere capacity in voltages up to 230 kv, it seems desirable to point out that there is now a need for 2,000 ampere capacity at 69 kv to match available circuit breaker ratings.

The switch described appears to offer the mechanical and electrical features desired for this type of equipment. It will be interesting to compare the service record in a few years with the present views on this development.

Reference

1. A New Outdoor Air Switch and the Principles Involved in Its Design, H. W. Graybill, J. S. Ferguson. AIEE Transactions, volume 64, 1945, pages 583–6.

F. C. Brogan (The Ohio Public Service Company, Cleveland, Ohio): This paper appears to go a long way toward crystallization of some of the thinking regarding design of air switches. The frequency of operation of these switches varies considerably in the many locations where they are used; also the weather and contamination conditions vary considerably, the main effect of which is a very wide variation in the operating duty imposed upon the switches.

A reduction in the number of parts is certainly a step forward, as is the reduction in number or elimination of braids or flexible shunts. Undoubtedly improvements have been made in the fabrication and lubrication of flexible shunts but troubles still crop up, particularly in locations where contamination is a serious problem, and the elimination of flexible braid is commendable.

The separation of spring member and current carrying member also appears to be an improvement, particularly since most metals or alloys do not rate high in both respects.

Both the electrical and mechanical properties of switches are important and it is agreed that increased short-circuit loadings on most systems place a greater duty on the switches, evidence of this having shown up in many instances in recent years.

We have had in service for over 20 years, so-called high pressure contact air-break switches, and with very little maintenance (principally in replacement of braid shunts) the operating experience has been very good. One of the principal faults with these switches was the difficulty of operating them manually, which was partially overcome by using unusually long swing operating handles.

The matter of contact pressure apparently is open to considerable difference of opinion but it appears that too high pressure involves too much force in the opening and closing; also may bring about spot welding effect between contacts (either due to limited contact area or too high contact pressure) and may result in wear and erosion of contact areas, particularly so if subjected to high currents and annealing of contact metals occurs. We believe that contacts designed with larger radii of the members and with only moderately high pressures might eliminate some of these difficulties. It is believed that the limitation in contact pressures in order to avoid excessively high operating effort might be overcome by the gear box and crank operating mechanisms now generally used, especially on the high-voltage switches. Use of gear box and crank mechanisms might permit a design of greater contact area with softer metals or alloys and retention of moderately high contact pressures without too severe contact abrasion.

We note that rolled silver inlays for one-half of the contact was tested against bare copper for the other half of the contact; also against a silver plated blade contact. Tests for the silver-silver contacts apparently indicated this was a very good combination which leads us to wonder why the silver inlay to copper combination was finally selected. We would expect from the report that silver-to-silver contacts would be the best design, particularly if silver inlays were practical for both members of the contacts without excessive abrasion. Silver plating in a laboratory test may prove very effective but in actual field service it is believed that the silver inlays for both halves of the contact would stand up much better providing contact pressures used are not excessively high and providing the area of contact is not restricted too seriously.

The approach to this design problem seems to have been very thorough, particularly in the attempt to co-ordinate experience and theory, and to meet more severe duty requirements. The tests indicate that some worth-while improvements have been made. Further tests, and especially service experience in the field should indicate whether some of the conclusions are correct.

A. H. Powell (General Electric Company, Schenectady, N. Y.): The author is to be commended for the fine way he has presented a comprehensive review of considerable information concerning contacts. On the basis of the information and actual test data, he has arrived at another design of contact. While the paper, according to the introduction, was intended to deal with both a new outdoor air switch and the broad subject of contacts, it is noted that only a short section of the paper actually dealt with the new switch and most of the information presented concerned contacts.

In connection with the new switch, it is noted that there is direct connection between the rotating insulator and blade through a so-called knuckle pin, and it would appear that rotation of the blade would cause vertical movement, although very slight, as the switch approaches the fully closed position. Such movement would be generally objectionable due to the resulting forces which may bend the blade, especially if ice is present. A diagram showing the torque required to operate the switch in relation to the blade position would have clarified this point.

Most of the paper deals with contacts and the author has given a bibliography of pertinent information and test data to point out the fact that a silver-to-silver contact from an electrical point of view is the best

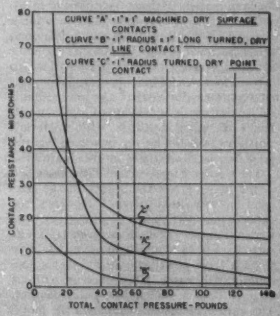


Figure 3

obtainable. However, it is further pointed put that under high pressure obtained with point contacts the soft silver surface is easily worn and the author concludes that a copper-to-silver contact would be superior. Figure 1 of the discussion shows the contact end of a new switch that was recently introduced to the industry, with the blade in the closed position before contact pressure is applied. Figure 2 of the discussion shows the contact after the blade has been rotated. Multiple flat copper fingers, each providing a short line of silver, backed by individual springs are used with approximately 50 pounds pressure on each contact line. The end of the blade is also silvered. With this construction the preferred silver-to-silver contact is obtained, the design prevents misalignment and distributes the pressure to prevent scoring.

Figure 3 of the discussion shows some interesting basic resistance information obtained in a laboratory with silver covered surface, point, and line contacts. Curve "B" shows that a pressure of approximately 50 pounds with a line contact 1 inch long results in practically the lowest resistance obtainable with a reasonable contact pressure.

Sulphiding of silver contacts is, of course, a problem and it was found nearly ten years ago that a silver button contact on a control switch had a resistance of 150 microhms when perfectly clean, but a resistance of 600,000 microhms when badly sulphided using currents in the order of 0.05 amperes. The resistance decreased as the current was increased up to a maximum of 100 amperes, when the resistance became 250 microhms. Higher currents would apparently have lowered it further.

This paper has confirmed other available information which shows that sulphiding of silver contacts in disconnecting switches operating at currents of several hundred amperes is probably not a serious problem. However, more information concerning dirty contact under short-circuit conditions would be very desirable.

S. C. Killian: The author wishes to thank those who commented on the paper and is pleased that it warranted such a complete discussion. Adams' six required points are of great importance and almost embody the basis on which the new switch was designed. The operating mechanism was not discussed because of space limitation. This is however an important factor and must be considered in any over-all switch design. The question regarding contact resistance versus time is a good one. Though no long-time tests were conducted to check this, it seems quite certain that holding a given current for a long length of time on a contact will not reduce its resistance. Operating voltage effects the resistance only when the voltage is very low and the film very thick. Then extremely high readings are obtained say at six volts. If the film is broken down by, say 20 volts, lower readings are immediately obtained. These are then further reduced by larger currents at six volts as shown in Figures 5, 6 and 7 of the paper. Conclusions reached in the paper are verified by the interesting heat runs conducted by Philadelphia Electric Company. Air switches, 2,000 ampere 69 kv, similar to Figure 2 of the paper are designed and have been in operation on the Buffalo-Niagara system for several years.

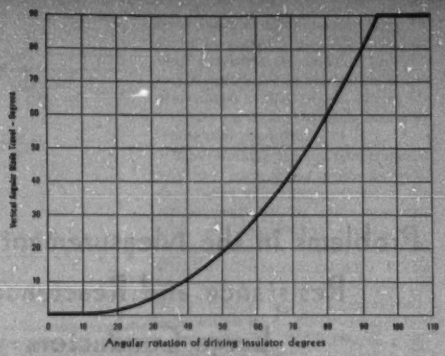
Owens' remarks on open contacts might seem important until one realizes that one contact must withstand adverse atmospheric and weather conditions. If the break contact is good enough to do this and it is agreed that it is a satisfactory contact, it is only sensible to design the hinge contact in a similar manner. A big majority of utilities and manufacturers now feel that open contacts which cannot trap snow and ice are to be preferred to covered contacts. Very large openings and clearances are used at both contacts of the new switch and it has passed all conceivable ice tests with no difficulty. Ice which collects around bearings is broken in tension and shear on well designed switches and is consequently of little importance. Short-circuit tests were made on the new switch but on clean contacts, just as all other tests in the country are run. To quote from the paper "Further testing on the behavior of various filmed contacts under short-circuit conditions is planned for the near future."

The remarks in Brogan's opening paragraphs are very well taken. The elimination of braids on air switches has been a definite step forward, as the almost universal adoption of the braidless switch by most utilities and manufacturers will attest. Contact pressures must be high enough to keep the contact parts in intimate contact under short circuit, or sputtering, arcing, and burning will occur. High operating effort can be avoided by securing mechanical advantages not only at the handle or operating gear box but in the live switch mechanism itself. The new switch contacts use silver inlays against copper because silver-to-silver galls at pressures which it is felt are required to successfully withstand short circuits.

Powell's point that the switch blade does have a very slight vertical motion is true. A travel diagram is shown here as Figure 4. The blade rise in the first 15 degrees of motion necessary to relieve contact pressure is less than one degree. This motion is so small as to be unimportant when compared to the large axial rotation of the blade in the powerful ice breaking movement. It is felt that the 50-pound contact pressure mentioned by this discussion is insufficient to provide a contact that will not burn and pit under bad atmospheric conditions and heavy short circuits, even though all silvered. Observations on line contacts are interesting, but definitely only on the academic side. It is virtually an impossibility to get a line to lie flat on a plane. On a nonprecision device such as an air switch where the blade is usually hinged several feet from the contact, misalignment is always present. This, even in very small degree, will allow the line to contact the plane at only one point, thus providing a poor point contact. The values shown in Powell's Figure 3 can have no significance for he is dealing with clean contacts and a difference of only a few microhms.

In his next sentence he mentions sulphided silver contacts, quoting values of hundreds and thousands of microhms. A few microhms difference is microscopic where filmed contacts are concerned and all contacts are filmed on switches for service, even before they are removed from the crate.

Figure 4. Blade lift versus rotation of driving insulator on new switch



Problems in the Measurement of A-C Resistance and Reactance of Large Conductors

E. H. SALTER
MEMBER AIEE

IN connection with their project to study the a-c resistance and reactance of large conductors in steel pipe and conduit, the Insulated Power Cable Engineer Association laid out a series of conditions under which measurements were to be made of four types or sizes of insulated conductors. This series originally consisted of measurements to be made under isolated conditions in air (single phase); with three conductors cabled as in a 3-phase installation, though in air; and finally with three cabled conductors pulled into various sizes and types of conduit or pipe.

This paper records experiences with problems which arose in connection with these measurements and the methods by which the difficulties were resolved. The four major problems covered herein are:

1. Can the in-phase (resistance) component of measured voltage drop along individual conductors be used for the computation of power loss as an I^2R loss?

2. What is the significance of the wide differences among the measured values of resistance for the individual conductors in 3-phase tests?

3. How should the potential leads, for measurement of IR and IX drops along the conductors, be brought away from the cable to the potentiometer?

4. Can the component losses due to the several elements be measured or evaluated?

The resolution of problem (3) led to an examination of losses set up in neighboring conducting and magnetic materials and the methods of eliminating or evaluating such losses.

The potentiometer circuit used in making the measurements is described in some detail as having some value for future reference. The results obtained are examined not only for the resolution of the problems enumerated, but also for certain minor problems. For example, do the a-c resistance and reactance change with the measuring current, over the range of currents used for the measurements?

Previous experience in a-c resistance measurements[1] indicated that lengths of about 25 feet should be satisfactory. Accordingly the conductors, each insulated with 0.125-inch of dry paper and finished with a serving of friction tape, were cut into pieces 28 feet in length and copper lugs attached at each end. The central 15-foot portion of each length was designated as the test section and potential points were established with that separation. At each potential point a hole was drilled through the insulation and into the conductor to a depth of about 3/8 inch. This hole was then tapped and an 8/32-brass machine screw was screwed down tightly into the thread. When conductors were set up for test, potential leads were then soldered to the heads of these screws.

Test Methods

When arranged for single-phase measurements, two test conductors were set up to form the opposite sides of a rectangular loop. The fall-of-potential along each conductor between the potential points was measured in terms of resistance and reactance drops by means of an a-c potentiometer, with resistance and mutal inductance, connected in the secondary circuit of a current transformer, the primary winding of which was connected in series with the conductor being measured. A schematic diagram of the general test circuit in this application is shown in Figure 1.

The use of this potentiometer arrangement makes it possible to determine the equivalent resistance and reactance of the conductors directly in terms of a standard resistance and standard mutual inductance. When the following symbols are used:

T = current transformer ratio
M = setting of mutual inductor, henries
S = standard resistor, ohms
P = total resistance of potentiometer, ohms
N = resistance of that portion of the potentiometer inserted to obtain a balance, ohms
K = tangent of angle introduced by current transformer
W = angular velocity, radians per second

the relations expressing the effective resistance, R, ohms, and reactance, X, ohms, of the length of conductor under investigation are:

$$R = \frac{1}{T} \cdot \frac{SN}{S+P} - \frac{WMK}{T}$$

and

$$X = WL = \frac{WM}{T} + \frac{K}{T} \cdot \frac{NS}{S+P}$$

Naturally, the measured reactance is dependent upon the loop formed by the potential connections. Figure 1 indicates that several arrangements were used in the full conduct of these tests. Reactance values for single-phase operation were reported, however, only for measurements made with the potential connections tightly bound against the surface of the conductor insulation. These values then are reported as the 60-cycle reactance "to the surface of the insulation."

Since the reactances measured generally were of the order of twice the resistance, the use of current transformers with phase angles not larger than three minutes

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E. H. SALTER is a research engineer with the Electrical Testing Laboratories, Inc., New York, N. Y.

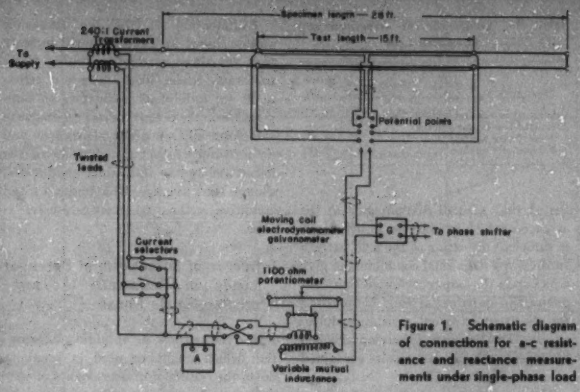

Figure 1. Schematic diagram of connections for a-c resistance and reactance measurements under single-phase load

of arc ($K = 0.001$) makes the last term of each of these expressions negligible. The relations then become:

$$R = \frac{1}{T} \cdot \frac{NS}{S+P}$$

$$X = \frac{WM}{T}$$

which are the equations used in the determination of effective resistance and reactance in these measurements. The use of a reflecting galvanometer of the electrodynamometer type, with the fixed coil excited through a phase-shifter, made it possible to balance the potentiometer to better than ±0.1 per cent.

By means of a heavy-current switch, a change could be made quickly from the a-c potentiometer to a d-c double bridge, with suitable current source, for the measurement of the ohmic resistance of the conductors. The use of a suitable reflecting galvanometer with the Kelvin bridge made it feasible to measure these resistances to better than ±0.1 per cent.

The arrangements for 3-phase measurements followed the same general pattern as for single phase. Naturally, three test conductors were used for each 3-phase setup and these were connected together at one end to form a Y or star load on the supply. Currents then were adjusted to approximate a balanced condition and again measurements of fall-of-potential were made of each conductor in terms of the current in that conductor and the results averaged. The connections used are shown in Figure 2. Computation from individual balance indications then yield effective resistance, ohms, per phase and reactance, ohms, per phase, line to neutral.

Measurements were first made with the conductors at or near room temperature using only sufficient current to obtain the desired sensitivity of balance. Temperatures above room temperature were obtained by operating the cable under load until a steady-state condition had been established, and then making the desired observations at that current. From 8 to 16 hours was allowed to reach this steady temperature condition.

When working at room temperature it was the practice to measure the d-c resistance before and after each set of a-c measurements and the results were averaged. At temperatures above room, measurements of d-c resistance were made after the a-c measurements only. In this latter case, the usual practice of noting the time at each of a series of repeated observations made it possible by extrapolation to determine the d-c resistance corresponding to the time of opening the a-c circuit.

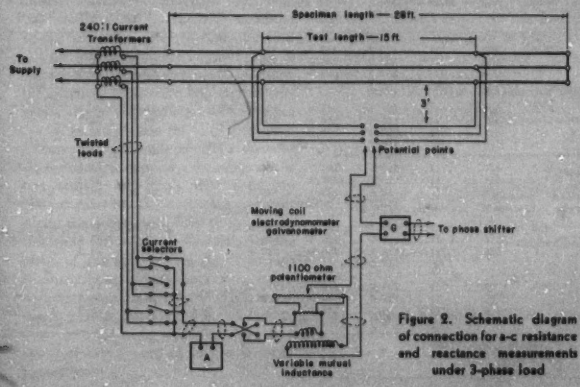

Figure 2. Schematic diagram of connection for a-c resistance and reactance measurements under 3-phase load

Accuracy of Resistance Measurements

Quite early in this work difficulties in interpreting some of the results obtained in the 3-phase measurements brought the accuracy of the measurements into question. Two series of tests therefore were conducted to demonstrate the degree of precision that might be expected in measurements made by the method described.

In the first of these checks a 4-terminal standard resistor, which had been constructed in 1910 for alternating current use and had been subject to periodic calibration since that time, was inserted in the heavy-current circuit in series with one of the conductors, and its resistance determined by the potentiometer method. Two sets of measurements were made in this manner, one in the single phase setup and another when arranged for 3-phase measurements. These results are shown in Table I.

This check showed a probable maximum error of the same general order as the limit of balance of the potentiometer, since in these measurements the potentiometer balance readings were 133.1 and 133.2 ohms, respectively.

A second series of checks, fundamental in its implications, made under 3-phase operation, compared the computed losses in the three conductors, using the potentiometer determinations of resistance, with the losses as measured by wattmeter methods. For these measurements a reflecting-type dynamometer wattmeter was employed. Determinations were made first by the 2-wattmeter method, and then by the 3-wattmeter method. Instrument limitations made it possible to observe these wattmeter indications

Table I. Check of A-C Standard Resistor

Assigned value of standard	0.00010073 ohm
A-C potentiometer measurements	
In single-phase circuit	0.0001008 ohm
In 3-phase circuit	0.0001009 ohm
Indicated maximum error	0.2 per cent

only to ±1 per cent. The results are shown in Table II. Here again the measured and computed values are in agreement to within the limit of observation. These two series of tests led to the conclusion that the a-c potentiometer method as used was giving a probable accuracy at least as good as ±0.25 per cent in the measurement of a-c resistance.

Interpretation of Individual Conductor-Resistance Measurements in 3-Phase Tests

Table III, which gives the results of a typical 3-phase test, shows a wide difference between the resistance balance indications of the a-c potentiometer for the individual conductors making up the 3-phase system. Such apparent differences in the resistance of the three conductors making up the circuit were found in practically all 3-phase measurements, regardless of whether the conductors were in air or in conduit. It was just such differences which originally raised the question of accuracy of measurement. When the accuracy of measurement had been demonstrated, the question still remained as to just what caused this unbalance in a-c resistances.

Those in contact with the investigation made numerous suggestions as to possible causes of the apparent differences and each was investigated.

Chief among the suggestions were the possibilities of stray-field pick-up in the measurement circuit, actual current unbalance in the system, and the manner of making potential connections. A careful survey for stray-fields which might influence results led only to several minor modifications in the disposition of the measurement circuit and did not serve to modify the test results. Currents in the system purposely were unbalanced by as much as 20 per cent with a negligible effect on the measurements. The method of making potential connections was modified to provide contact with a greater number of the individual strands, but no difference in measured values could be attributed to this modification.

Finally, the suggestion was offered that the apparent differences in measured resistances might be due to mechanical dissymmetry between the several conductors as arranged for test. This theory suggested that a small dissymmetry in the arrangement of conductors would result in sufficient distortion of the field pattern surrounding individual conductors to give a pickup in the loop represented by a conductor and its potential leads and to cause the resultant apparent resistance differences. This theory was therefore checked by experiment and the results contained are shown in Table IV.

Table II. Comparison of Measured and Computed Loss

	Watts
Computed loss in test section	59.0
Measured loss in test section	
2-wattmeter method	59.5
3-wattmeter method	59.0

These measurements showed definitely that an additional dissymmetry in the placement of these conductors in the triangle for 3-phase measurements would cause additional unbalance in the apparent resistance of the individual conductors, but it had a negligible effect on the total measured loss or on the average resistance of the conductors. It was concluded therefore that such accidental dissymmetry was the initiating cause of this apparent unbalance, but that its effect on the resulting average resistance, determined by the method being used, was of no appreciable consequence.

Examination of the d-c resistance values shown in Table III will indicate that the three conductors heated fairly uniformly under load. The a-c resistances for individual conductors as computed from the potentiometer readings are therefore only apparent resistances and do not truly reflect the division of the losses among the individual conductors, though their average value under a given condition is the true average a-c resistance.

Influence of Disposition of Potential Leads on Resistance Measurements—Single Phase

For the purpose of obtaining information which might be used in breaking down the results of measurements in conduit into component influences and to demonstrate that the conductors used were normal as to the usually computable losses, it was desirable to make certain single-phase measurements in air. These measurements were to be made first of an isolated conductor, free from proximity effects due to other conductors, and then at various distances from a similar conductor in order to evaluate the proximity effect.

When arranged for the first condition, potential leads were carried back from the potential taps, along the surface of the conductor under study, to the approxi-

Table III. Typical 3-Phase Test Data

1,500,000 Circular Mil Concentric-Strand Conductor, in 5-Inch Conduit Triangular Arrangement

Avg. Cond. Temp., C	Test Current, Amperes	Cond. Measured	Measured D-C Resistance, Microhms	Resistance Balance A-C Potentiometer, Ohms* Dir.	Resistance Balance A-C Potentiometer, Ohms* Rev.	Computed A-C Resistance, Microhms	Reactance Balance, A-C Potentiometer, Scale Divisions Dir.	Reactance Balance, A-C Potentiometer, Scale Divisions Rev.	Indicated Mutual Inductance, Microhenries
16.5	300	I	102.2	253.4	251.6		35.7	35.1	
	300	II	102.3	299.9	288.1		39.3	39.0	
	300	III	102.2	222.7	218.3		46.3	46.9	
		Avg	102.2	255.7		193.5	40.4		262
27.5	300	I	106.7	255.6	257.3		35.4	34.6	
	307	II	106.6	296.6	305.5		38.7	38.7	
	317	III	106.8	226.2	220.6		46.3	46.4	
		Avg	106.7	260.3		197.3	40.0		263
36.5	475	I	110.2	263.1	260.2		33.6	34.1	
	451	II	110.1	317.8	310.9		38.3	38.3	
	468	III	110.6	223.1	226.2		46.5	46.3	
		Avg	110.3	266.9		202.2	39.5		266
54.0	619	I	117.1	263.9	265.0		33.4	33.0	
	614	II	117.4	320.4	326.7		37.3	37.5	
	626	III	118.1	236.8	233.4		46.0	46.2	
		Avg	117.5	274.4		208.0	38.9		270
73.5	759	I	124.8	270.6	270.3		32.7	33.1	
	755	II	125.0	334.5	329.4		36.6	36.6	
	750	III	125.7	245.1	247.4		46.0	45.8	
		Avg	125.3	282.9		214.3	38.5		273

* T = 1200/5 or 240
S = 0.2 ohm
P = 1,100 ohms

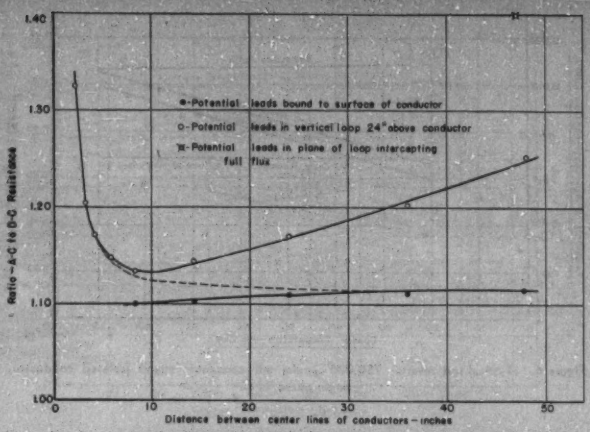

Figure 3. Effect of potential circuit arrangement on the measured a-c resistance in air, single-phase, at various conductor spacings

mate center of the test section and from this point were taken as a twisted-pair to the potentiometer measuring circuit. The plane containing the loop formed by the potential leads from a conductor and that conductor was in each case made perpendicular to the plane containing the loop formed by the heavy-current circuit through the conductor being tested. However, when the conductor under test was gradually brought closer to the other side of the loop, to determine proximity effects, it was found that measured a-c resistances began to decrease and at a spacing of six inches between conductors the measured resistance was several per cent lower than that for a spacing of four feet and was still decreasing.

This apparent decrease in resistance was contrary to the usually accepted theory and hence directed some question at the measurement. Accordingly, a second arrangement was made in which the potential leads were first carried perpendicular to the conductor for a distance of two feet, and then the loop completed by carrying these leads parallel to the conductor to a point opposite the center of the test section and from that point by twisted-pair to the measuring circuit. Again the plane of the loop of the potential leads was made perpendicular to the plane of the loop of the heavy-current circuit. Using this arrangement, measurements were made at various conductor spacings from conductors in contact to four feet between centers. Figure 3 shows results typical of such a series of measurements.

It will be noted that under this arrangement the a-c resistance ratio first decreases sharply as the conductor spacing is increased, reaches a minimum at a spacing of about ten inches and then increases with spacing. The decreasing portion of this curve is typical of what might be expected in the way of proximity effect, but the increase with increased spacing would not appear to be a conductor effect at all. To demonstrate, another potential circuit arrangement was used at the four-foot spacing; in this case the plane of the loop of the potential circuit was made to coincide with that of the current loop and the potential leads were carried to the center of this loop before running parallel to the conductors. In this position the potential circuit loop intercepted the full flux of the current loop. At this spacing the indicated a-c resistance was found to be much greater than with any other loop arrangement.

These tests were conducted in a steel-frame building with the test conductors in a plane only about two feet above the reinforced concrete floor slabs. The apparent added resistance under these latter conditions was no doubt due to the losses created by the test circuit in the building structure. However, it is of interest to note that, even though the four-foot loop was inducing losses in the building structure, these losses were measured only in proportion as the flux threading this structure also threaded the potential circuit loop. When this loop was closed by binding the potential leads onto the conductor, the external losses were not measured. When the potential was opened to avoid the effects of field distortion at close spacing of the conductors, maintenance of this same arrangement of potential leads at wider conductor spacings allows this loop to link with flux which enters the building structure and produces an apparent increase in measured resistance. The ultimate was reached with the final arrangement, when all flux linked the potential circuit loop.

The true variation of conductor resistance with conductor spacing is probably that shown as a dotted curve, tangent to the two other curves at their extremities.

As a check, one member of the advisory group suggested making the measurement of increase in resistance due to skin-effect by wrapping the potential leads spirally around the conductor. This method was tried and the results are shown in Table V. Here it was found that wrapping the potential lead with a left-hand lay gave values quite different than obtained when the wrapping was given a right-hand lay. The average of the two measurements, however, was found to be in good agreement with results obtained with the potential connections bound to the conduc-

Table IV. Effect of Mechanical Dissymmetry on A-C Resistance Measurements

2,500,000 Circular Mils Concentric Strand Conductors, 3-Phase, In Air Triangular Arrangement

Cond. Temp., C	Test Current, Amperes	Conductor Measured	Measured D-C Resistance, Microhms	Resistance Balance A-C Potentiometer, Ohms Dir.	Rev.	Computed A-C Resistance, Microhms
			As Originally Set Up for Measurement			
22.0	553	I	61.55	158.8	157.4	
	565	II	61.67	144.1	141.6	
	553	III	61.58	172.2	168.9	
		Avg.	61.60	157.0		119.0
			After Moving Conductor III about 1/2 Inch from Conductor II for 4 Feet of the Test Length			
22.0	553	I		158.8	157.3	
	560	II		124.7	123.2	
	545	III		188.3	191.0	
		Avg.	61.60	157.1		119.1

Table V. Single-Phase Determinations of AC-Resistance Ratio with Various Arrangements of Potential Leads

1,500,000 Circular Mil Concentric-Strand Conductors, In Air, 4-Foot Loop Arrangement

Disposition of Potential Leads		Ratio R_{ac}/R_{dc}
Potential leads bound to conductor surface		1.19₅
Potential leads in loop two feet above conductors		1.36₄
Potential lead spiraled around conductor:		
Lay of Spiral, inches	Direction of Spiral	
3	L. H.	1.22₉
3	R. H.	1.16₄
Average		1.19₇
4	L. H.	1.25₇
4	R. H.	1.13₁
Average		1.19₇
2	L. H.	1.29₈
2	R. H.	1.06₈
Average		1.18₁

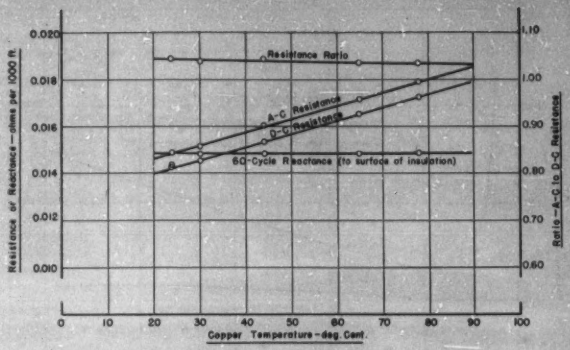

Figure 4. Typical test results. 750,000 circular mil concentric strand isolated conductor, single phase, in air

tor surface. These results only serve to confirm the original arrangement for measurements at the wide spacing.

Effect of Method of Connecting Potential Leads to Conductors Laid in Conduit—3-Phase

When three conductors were set up in conduit for 3-phase testing, the question again arose as to what procedure should be followed in bringing out potential connections. As in the case of 3-phase measurements in air, the three conductors were laid together in such fashion that the driven potential taps on the individual conductors lay in planes perpendicular to the axis of the cabled assembly. Individual potential leads were brought out from each potential tap.

For measurements in air, these potential leads had been brought away from the cables, for a distance of about 24 inches, perpendicular to the axis of the cable. At this distance the leads had then been carried parallel to the test conductor toward the center of the test section where mate pairs were twisted together and led away to the measuring circuit. To follow this same procedure when the conductors were placed in conduit meant bringing these leads out through holes in the conduit (the 15-foot test length of conductors was centered longitudinally in the 20-foot length of conduit). The question arose as to whether bringing the leads through this magnetic circuit (iron conduit) would in any way give a linkage that might affect the results.

To check this the first conductors set up in conduit were provided with two sets of potential connections. One set was brought out through holes in the pipe as described before and the other set was carried along the conductors to the ends of the conduit and thence out. Two sets of

Table VI. Effect of Current Magnitude on Measurements of A-C Resistance 3-Phase Operation in Conduit

Conductor Current, Amperes	Cable Temp, C	Measured D-C Resistance, Microhms	Computed A-C Resistance, Microhms	Ratio R_{ac}/R_{dc}	Derived Reactance, Microhms
1,500,000 Circular Mil Concentric-Strand Conductors, 3-phase Operation in 5-inch Conduit					
295	21.0	104.0	194.5	1.87₁	412
455	22.0	104.5	195.0	1.86₈	413
560	23.5	105.1	195.9	1.86₉	415
675	24.5	105.5	196.3	1.86₁	416
785	27.0	106.6	197.3	1.85₁	418
2,500,000 Circular Mil Concentric-Strand Conductors, 3-phase, 6-inch Conduit					
365	25.5	62.5	161.7	2.59	386
595	26.0	62.5	162.3	2.60	386
830	25.5	62.4	162.9	2.61	375
1,035	23.0	61.8	162.2	2.63	386
2,500,000 Circular Mil Segmental-Sector Conductors, 3-phase, in 6-inch Conduit					
360	26.5	65.5	124.3	1.90	440
505	26.7	65.6	123.6	1.88₅	440
665	27.5	65.7	123.7	1.88	443
855	29.0	66.1	124.9	1.88₉	443
1,015	30.5	66.6	124.3	1.87	443
1,215	33.0	67.1	126.0	1.87₇	445
430	64.0	75.5	132.1	1.75	443
605	63.5	75.3	134.3	1.78₄	445
775	63.5	75.3	133.7	1.77₁	445
945	64.0	75.5	133.2	1.76₄	445
1,130	64.5	75.7	133.7	1.76₄	445
1,310	65.0	75.9	133.7	1.76	445

Table VII. Analysis of Transient Temperature Rise Data to Determine Power Loss Resident in Conduit

Condition	After 15 Minutes Operation	20 Minutes Operation
Conduit temperature (centigrade) rise with a-c heating		
26.4 watts per foot, cable	0.65	1.0
28.2 watts per foot, computed	0.7	1.05
Conduit temperature (centigrade) rise with d-c heating		
28.2 watts per foot (cable)	0.2	0.35
Incremental temperature (centigrade) rise		
28.2 watts per foot (cable)	0.5	0.7
Conduit temperature (centigrade) rise with self heating		
9.4 watts per foot	1.9	2.35
Computed watts per foot self heating in conduit with 28.2 watts per foot delivered to cable	2.5	2.8
Relative value of watts self heating in conduit as per cent of total power delivered to system	9	10

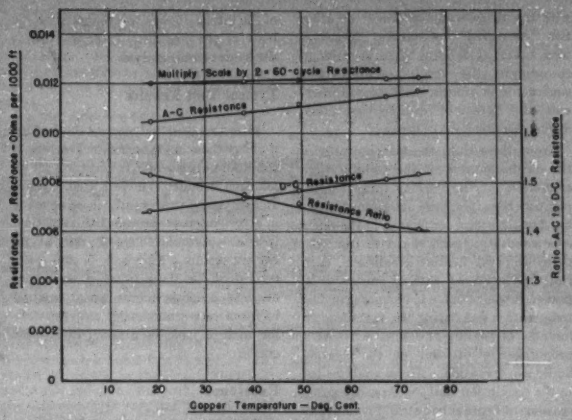

Figure 5. Typical test results. 1,500,000 circular mil concentric strand conductors, triangular arrangement, 3-phase, 1.70 inch spacing, in air

observations were made, one for each system of potential leads. The individual conductor measurements for the two systems were widely different, but again the average values obtained were in close agreement. The data obtained using the leads brought out through the holes in the pipe showed variations between individual conductors which were comparable with those obtained for the conductors in air, but when using the leads brought out at the ends of the pipe the spread between individual values was much greater, one conductor even indicating a negative resistance in this case.

Based on these findings all of the tests in conduit or pipe were made with potential leads brought out through holes in the pipe.

Effect of Current Magnitude on Measurements of A-C Resistance in Conduit—3-Phase

The various cable temperatures obtained for test conditions in this investigation were produced by circulating current through the conductors as arranged for test. Measurements then were made with this value of heating current in order to assure stable temperature conditions during this measurement process. As a result each test point was taken under a different current loading condition. This naturally raised the question as to what variation might be expected due to the magnitude of the test current and whether this might have to be taken into consideration in arriving at the final results. This was especially questioned where tests were run in conduit which might saturate magnetically and thus influence results. Tests to determine the magnitude of such influence were run and the results are shown in Table VI.

The general conclusion indicated was that current magnitude has a relatively small influence on the a-c resistance value compared to the influence of temperature and hence can be neglected in analyses of these measurements.

On the other hand, it is indicated that probably most of the change in reactance found is a function of current magnitude rather than of cable temperature. However, the change in reactance with current in many tests was found to be quite negligible and amounts to only a few per cent in any instance.

Separation of Losses in Conduit from Total Losses

In the analysis of these data it became desirable to separate the total losses into the losses in the conductors and those in the conduit. This was done from comparative time-temperature studies of the conductors and of the conduit under the transient heating of cable and conduit immediately following: a-c loading of the cables; d-c loading of the cables; and d-c heating of the conduits. Under the second condition it is evident that all of the heat was generated in the conductors themselves, while in the third, all of the heat was generated in the conduit.

This study was made using three 2,500,000 circular mils concentric-strand conductors arranged in the conduit in the same manner as for the a-c resistance measurements. Spot temperatures of both conductors and conduit were measured with copper-constantan thermocouples in conjunction with potentiometer-type temperature indicators arranged for use with box-type reflecting galvanometers to give a sensitivity of about 0.2 centigrade. Six thermocouples were laid against the individual copper conductors, and covered with the paper insulation, distributed between the three conductors at two locations about ten feet apart. Another six couples were

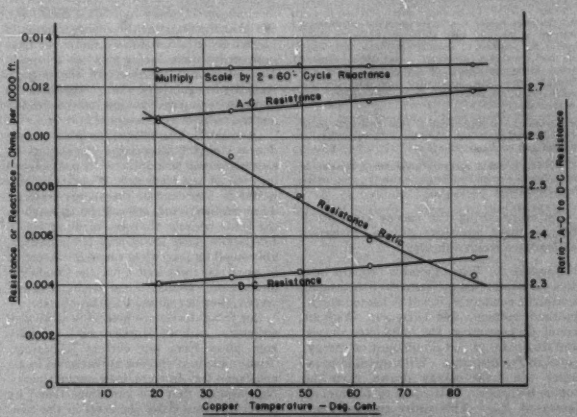

Figure 6. Typical test results. 2,500,000 circular mil concentric strand conductors, triangular arrangement, 3-phase, 2.17 inch spacing, in 6-inch conduit

bound to the surface of the insulation of these conductors. Eight thermocouples were attached to the outer surface of the conduit by means of Scotch tape, these being distributed both laterally and circumferentially. A mercury-in-glass thermometer was likewise placed in contact with the outer surface of the conduit.

Average temperatures were obtained from resistance measurements. Such measurements were made of the conduit in all tests and of the conductors also in the case of d-c tests. In the a-c tests the normal measurements of a-c resistance were made.

A reference body for ambient temperature was supplied by another length of conduit supported at the same level as the test conduit and about 18 inches away. The temperature of this body was measured in the same manner as for the test conduit.

The tests were run with a timekeeper and five observers. Three observers made the temperature determinations with the thermocouples, while the other two measured resistances. Readings were taken at definite times in order to co-ordinate the results. Observations were made in groups, at 20-second intervals, and 12 sets of readings were taken during the first hour of heating.

From these data the temperatures of the conductor and of the conduit were computed for the various times. Temperature rises then were determined, making due allowance for change in the reference conduit. Using the data from the three different heating conditions, each run with a known rate of energy dissipation, it was possible to compare conduit temperature rise during the first 15 to 20 minutes of heating and thus arrive at a proportionate division of the heating under a-c operation. These analyses led to the conclusion that for three 2,500,000 circular mil concentric-strand conductors in triangular arrangement, with 2.17-inch spacing between conductor centers, laid in six-inch iron conduit, 9 to 10 per cent of the total a-c losses was generated in the body of the conduit. Table VII shows the steps of this analysis.

Typical Test Results

After a method had been established for making these measurements a series of conditions in air and in conduit was set up for each conductor investigated. While an analysis of these results is being made in other papers presented at this meeting, it seems appropriate here to present typical test results. Figures 4, 5, and 6 are typical of the three types of tests conducted. Smooth curves were prepared for each test arrangement and the data for analysis have been taken from these curves.

Reference

1. Resistance and Reactance of 2-Conductor Cables, E. H. Salter, G. B. Shanklin, R. J. Wiseman. Electrical Engineering (AIEE Transactions) volume 53, 1934, December section, pages 1581-9.

Discussion

L. F. Roehmann (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): Salter's paper is an excellent presentation of difficulties and perplexing results which may be encountered by any electrical engineer engaged in experimental work. I was pleased because usually difficulties of this kind are glossed over or entirely omitted in a discussion while here they form the basis of the paper.

As I see it, Salter and his sponsors wrestled fundamentally with the following three questions:

1. When a heavy conductor carrying alternating current is brought into close proximity to some metal, for instance to another conductor, why does its a-c resistance increase? And subsequently: when three conductors of a 3-phase system are brought into close proximity, how does the a-c resistance of the individual conductors change, and of their sum?

2. Does the arrangement of thin potential leads (which carry no current) affect the a-c resistance of an isolated conductor?

3. When a cable carrying alternating current is installed in a metallic pipe, how can the pipe losses be determined?

By emphasizing the energy concept, I believe these questions can be answered without too much difficulty.

Question 1. When metal is brought into an a-c field, for instance into the field around a straight conductor, it will be heated due to induced voltages and currents. Without metal in proximity, the conductor draws, and dissipates, a certain amount of energy, equal to I^2 times R_{ac}. With metal in proximity the total energy drawn from the source increases. It is equal to I^2 times an increased R_{ac}. This entire energy is not expended in the conductor. Part is handed on to the metal and expended there. This is nothing but the well known transformer. The energy input at its primaries is not expended within the transformer—this would be zero efficiency, but is essentially handed on to the connected load. A conductor with some metal nearby is of course a very inefficient transformer, but still its efficiency is not zero. Part of the input energy is handed on and the a-c resistance at the conductor (but not of the conductor) increases.

In case of three conductors of a 3-phase system, each conductor can be considered as a primary, inducing currents (that is, losses) in the two neighboring conductors. Spacing, obviously, plays an important role in the amount of energy transferred and consequently affects the a-c resistance measured at (but not of) each individual conductor. While in the first case the energy is actually handed on to an outside consumer, we have now an intra-conductor play, so to speak. No energy is transferred to the outside. As a consequence, the sum of the three a-c resistances remains constant, however different the individual resistances may be.

Question 2. Considering an isolated conductor carrying alternating current, its resistance should be unaffected by position and location of the leads. Any such variation would be contrary to the energy concept. Theoretically, currents will be induced in the leads because they are made of metal. Practically such losses will be immeasurably small for lead wires whose cross-section is small as compared with the conductor under test. Hence, if variations were observed, they are caused by other effects.

Our laboratories are housed in a wooden structure, and all services were carefully kept away from our current laboratory. Tests made in it showed no variation in a-c resistance for different lead arrangements. No question that the variations found by Salter are caused by the steel in the reinforced-concrete floor which was only two feet below the conductor under test.

Question 3. Conduit losses were determined by Salter but in a fairly round-about manner. The discusser has used, in 1943, a simple method as follows (for single conductors):

a. Bring twisted potential leads through a hole in the conduit, spread them apart inside, connect to cable, and measure R_a, Figure 1 of the discussion.

b. Bring twisted potential leads up to pipe, spread them apart outside, take through two holes in conduit, connect to cable, and measure R_b.

c. Calculate conduit loss as $I^2 (R_a - R_b)$.

This procedure is straightforward, and in addition, pedagogically interesting. In both cases the a-c resistance *at* the conductor is measured but only in connection (a) the resistance *of* the conductor. Connection (a) determines the energy expended in the conductor, and connection (b) the energy expended in conductor plus pipe. Obviously the difference is directly the pipe loss.

E. E. Hutchings (British Electrical and Allied Industries Research Association, London, England): Referring to the four major problems specified on page 1 of Salter's paper.

1. The measured a-c resistance will differ as between the individual cables of a 3-phase system if these are not symmetrically ar-

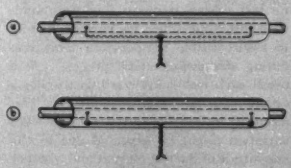

Figure 1. Lead measurements for measuring pipe losses (one conductor)

ranged, owing to phase transference of power through the magnetic field.[1] This has no influence on the heating, which is equal in all three cables. For balanced currents, the total losses are given by $3I^2R$, where R is the mean value of a-c resistance in the three cables.

2. The difference in measured value of a-c resistance between the individual conductors is in part due to the phase transference of power referred to in 1. and in part to asymmetry of the conductors with respect to adjacent metalwork.

3. The ideal method of running potential leads for such measurements as are described in the paper is to locate any portion of such leads which is parallel to the cables at an equal distance from each of them. In practice, this may be done by running leads back from the star point of the conductors at a distance from them of not less than 50 times the distance between their centers, since the magnetic field due to the three cables at such a distance is negligible.[1] In similar tests made by the British Electrical and Allied Industries Research Association, the potential leads were run from 15 to 20 feet above the cables. The symmetry of the arrangement of current carrying conductors can be checked by passing single-phase current through two of them and measuring the potential induced in the third and its potential leads, which should be negligible in comparison with the volt drop along this conductor when it is loaded. The results of Figure 3 of Salter's paper can be attributed to a nonsymmetrical disposition of the potential lead in relation to the two current carrying conductors. The normal variation of proximity effect with spacing would be observed if this lead had been disposed as recommended above. The results given in test number 1 in Table I of Wiseman's paper[2] agree well with the theoretical calculation of skin effect, since in this test the spacing is such that proximity effect would be negligible. The additional increment of resistance due to the latter effect in test number 2 is, however, very much greater than would be expected on theoretical grounds at the spacing indicated. Proximity effect for three cables in triangular formation in air, closely spaced, is given to a close approximation[4] by the expression

$$y_1 = 1.5\ (d/s)^2 y$$

where

d = diameter of the conductor
s = axial spacing between conductors
y = increase due to skin effect.

4. The losses in the conductors in air, in the absence of adjacent metalwork, can be measured and compared with theory on well established principles.[1] Very close agreement has been obtained in tests, similar to those described in the papers, which were carried out by the British Electrical and Allied Industries Research Association.[5] The increase in resistance due to proximity to steel in the form of plates parallel to the conductors is less than 0.002 microhm per centimeter at 12 inches from the steel for closely spaced conductors (as in a pipe) and is independent of conductor size.[6] This is less than 2 per cent for the largest conductors considered in the AIEE papers. The excess losses when the conductors are run in a steel pipe can be compared with theoretical values calculated as for steel armoured cables by known methods[7] using the appropriate properties of the steel. Only an approximate value need be assigned to the permeability, since variation of this over a wide range has but little effect. Unfortunately, only the nominal diameter of the pipes and their resistivities are given in the present papers: full dimensional data are required in order to check the experimental results against the theory.

References

1. **Wedmore, Whitehead, Morgan.** *Journal*, Institution of Electrical Engineers (London, England) volume 67, March 1929, page 367.

2. **Brockbank, Webb.** *Journal*, Institution of Electrical Engineers (London, England) volume 67, March 1929, page 339.

3. A-C Resistance of Large Size Conductors in Steel Pipe or Conduit, **R. J. Wiseman.** AIEE Transactions, volume 67, part II, 1948, pages 1745–58.

4. **Whitehead, Hutchings.** *Journal*, Institution of Electrical Engineers (London, England) October 1938, page 519.

5. **Whitehead, Morgan, Hutchings.** Report Ref. F/T61 (B. E. A. I. R. A.).

6. **Booth, Hutchings, Whitehead.** *Journal*, Institution of Electrical Engineers (London, England) volume 83, 1938, page 497.

7. **Whitehead.** *Journal*, Institution of Electrical Engineers (London, England) volume 88, part II, February 1941, page 64.

E. H. Salter: The problems discussed in this paper arose in an investigation to determine the circuit constants of certain large conductors operated as a 3-phase transmission system in iron pipe or conduit, and in the experimental analysis of certain of these findings. Definite limitations were placed on the extent of the investigation due to the problem of defraying the costs.

Measurements were made of only four sizes or types of cable under current loadings covering the range indicated in Table VI. Here the lowest current load in each case was that required to give adequate sensitivity in measurement and the other values were the load currents required to give heating to produce the conductor temperatures as shown in Figures 4, 5 and 6. The purpose of Table VI then is to show that measurement at these various currents did not influence conductor characteristics over the temperature range investigated.

All measurements of conductors in pipe were made with the pipe insulated. No attempt was made to determine voltages that might have appeared along the pipe. Since measurements were made by potentiometer using an a-c galvanometer as a detector, any harmonics which may have appeared due to nonlinearity of the magnetic circuit did not enter into balance conditions.

The suggested analysis of some of the measurement problems by emphasizing the energy concept serves only to reiterate the analysis in the paper of the influence of disposition of potential leads on resistance measurements, single phase. In addition to energy losses in neighboring metal structures, it becomes necessary to take into consideration changes in current distribution in a conductor due to internal reactions (skin effect) and to the flow of current in other near-by conductors (proximity effect), which in each case affect the resulting a-c resistance. Then too, in 3-phase measurements any loop provided for measurement of one conductor has linkage with flux from the other two conductors as well. Since the currents in these other conductors are out of phase with the current in the conductor under measurement, such linkages may give in-phase or resistance components to the measured drop. Only when the potential loop is made to extend a considerable distance from the conductors will the fields of the three conductors effectively neutralize and the resultant of these in-phase components be at a minimum. Accuracy of measurement would dictate that the truest average is obtained when each of the three observations is closely alike. If in 3-phase measurements, the potential leads were carried back close to the conductor group this would not be the case. Hence in all 3-phase measurements the potential leads were brought out at least 24 inches away from the cables and then carried parallel to the group to close the loop.

The suggestion for measurement of conduit losses would be subject to this same criticism. Any measurement made with potential leads forming a loop inside the conduit would give a doubtful measure of energy expended in the conductors of a 3-phase system, due to the close proximity of the small potential-circuit loop (thus formed) to the individual conductors and to the failure of fields to neutralize at that distance

Military Teletypewriter Systems of World War II

F. J. SINGER
MEMBER AIEE

THE TELETYPEWRITER was a vital communications tool of World War II. Secret teletypewriter communications to permit close and efficient coordination between all echelons, from comparatively small tactical units to the highest military and diplomatic leaders, were handled with accuracy and dispatch on a very efficient basis. Wire and radio teletypewriter networks, extending over land and sea were used intensively by our armed forces and Government officials for handling the stupendous tactical, supply, and administrative problems, as well as the affairs of state, brought about by the war. These teletypewriter communications became a reality as a result of the development since late 1941 of improved teletypewriter cipher arrangements and of means to permit dependable teletypewriter operation, not only over short-haul radio and military wire-line networks, but also over transoceanic and other long-haul radio circuits.

This paper reviews the evolution of military teletypewriter communications since 1941 and briefly describes some of the important systems that were developed during the war by Bell Telephone System engineers for the armed forces.

Telegraph Communication Systems as of 1940–41

Commercial Wire-Line Systems in the United States

Immediately prior to the Pearl Harbor attack the large amount of commercial and the relatively small amount of military telegraph communications within the United States were handled mostly over wire lines. Teletypewriter, rather than manual keying methods, were used in the majority of these communications. Through successive improvements made in teletypewriter apparatus[1] and in the d-c[2,3] and carrier telegraph[4,5] line facilities since World War I, it was practicable to send several teletypewriter messages simultaneously over long wire-line circuits at speeds of 60 words per minute and higher.

The Western Union Telegraph Company and Postal Telegraph Company utilized teletypewriters for communications between their local offices and main distributing centers in each city. These companies passed their intercity public message and their large customers' leased circuit traffic over carrier and d-c telegraph channels generally using either synchronized time-division multiplex telegraph methods[6] or variations of this method,[7,8] thereby obtaining very efficient use of the line facilities. With synchronized time-division multiplex, commutating devices provide a means whereby two or more messages are sent over a single telegraph channel.

The Bell Telephone System furnished teletypewriter exchange (*TWX*) [9,10,11] and private line manual Morse and teletypewriter[11,12] services, providing direct communication between two or more stations in each customer's connection or network, as is done in telephone service. With direct connections the sender's message is recorded at all customers' offices in the network as soon as the message is sent.

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F. J. Singer is switching development engineer with the Bell Telephone Laboratories, Inc., New York, N. Y.

The Bell Telephone System and telegraph companies also furnished private-line switched teletypewriter service on a leased network basis[11,13,14] to some of the large users of teletypewriter service. This form of service permits a very rapid handling of messages and a high traffic-carrying capacity of the intercity trunks used in the network.

Overseas Telegraph Systems

Prior to the war telegraphic communications between the continental United States and foreign countries were handled by radio and submarine cable. Traffic was handled on an efficient basis over high-speed submarine cables[15] using the synchronized time division system.[16] The long-haul radio links employed international Morse code methods generally using perforated tape sending and dot-dash inked tape receiving.[17] With this tape method it is necessary to read the receiving tape and translate the information manually into typed messages. Teletypewriter methods[18,19] were applied only to a minor extent over long-haul radio because of transmission difficulties.

Systems Used by the Navy

The Navy operated a rather extensive network between shore stations, between ships and between ships and shore. Practically all of the Navy traffic was handled by manual international Morse code telegraphy. The Navy had not adopted the teletypewriter although to care for large traffic over their main communication routes they used the perforated-tape-sending and inked-tape-receiving method of transmitting international Morse code signals. Skilled operators and reasonably good radio paths were required and the latter were not always available. It was, of course, necessary to transcribe manually incoming dot-dash messages at the receiving end of the circuit

Systems Considered by the Army

Because of restrictions imposed by peacetime conditions the Army used only a relatively small amount of wire-line teletypewriter communications facilities within the United States. These condi-

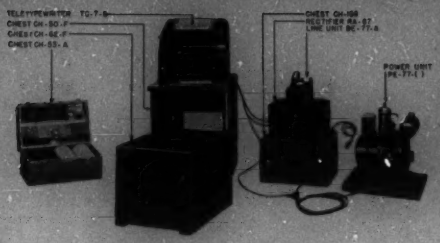


Figure 1(a). Teletypewriter set, *EE-97-A*

Figure 3(a). Telegraph central office set *TC-3*

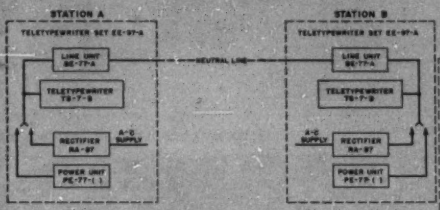


Figure 1(b). Application of teletypewriter set, *EE-97-A*

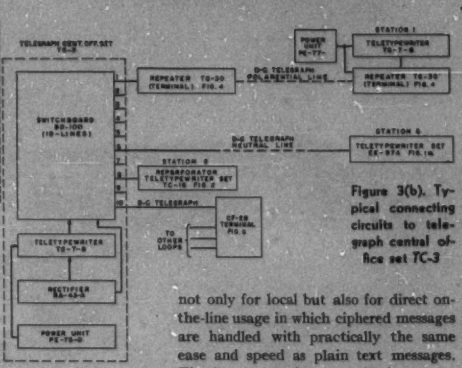


Figure 3(b). Typical connecting circuits to telegraph central office set *TC-3*

tions made it very difficult for the Signal Corps to do more than consider potential communication networks for war purposes. At the beginning of World War II the Signal Corps had worked out comprehensive plans for handling wire-line telegraph and teletypewriter communications. Work had been done in co-operation with the Teletype Corporation engineers and other groups toward the development and production of portable military teletypewriter equipment. The trend at that time indicated, however, the use of slow manual Morse methods and not teletypewriters over radio.

Evolution and Advantages of Cipher Teletypewriter Systems for Military and Diplomatic Communications

During World War I Bell Telephone System engineers recognized the advantages of the teletypewriter as a particularly suitable means of providing military and diplomatic communications. Indeed it was during this period that they developed the method of locally enciphering and deciphering teletypewriter signals.[20–24]

With the advent of World War II it was natural for the Bell Telephone Laboratories and Teletype Corporation engineers to advocate the use of ciphered teletypewriter arrangements in military and diplomatic communications. With close co-operation of the Army, modernized teletypewriter enciphering and deciphering arrangements were quickly developed not only for local but also for direct on-the-line usage in which ciphered messages are handled with practically the same ease and speed as plain text messages. The Army started to apply both arrangements soon after Pearl Harbor.

Teletypewriter cipher communication methods have the outstanding advantages of secrecy, dependability, ease of training operators, speed in handling messages, and of accurate and efficient use of the transmission medium. Ordinary telegraphy, coded words of speech and other cipher systems do not have all of these advantages. The advantages of cipher teletypewriter communications were generally recognized by the War and Navy Departments and by the other departments of the Government and their use over both wire and radio networks rapidly became the backbone method of military communication during World War II, especially where long distance communications were involved and where conference connections were desired.

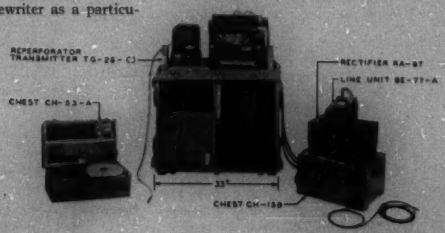


Figure 2. Reperforator teletypewriter set *TC-16*

-C and Carrier Telegraph Systems for Wire Lines Used in World War II

It was recognized at the outset of World War II that military wire-line telegraph systems must be capable of withstanding extreme and widely fluctuating tempera-

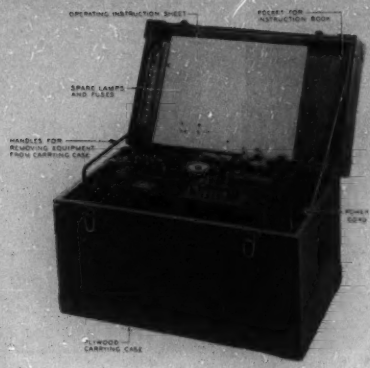


Figure 4 (left). Telegraph repeater TG-30 (Terminal)

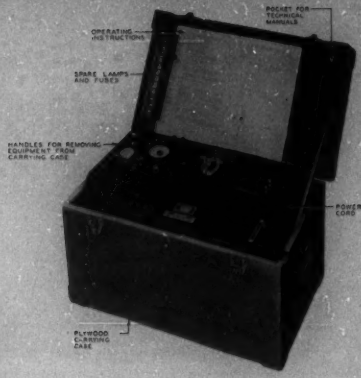


Figure 5. Telegraph repeater TG-31 (Intermediate)

ture and humidity conditions, must be sufficiently rugged for transportation at least into and through the theatres of operation if not in combat areas, must be capable of operating from a-c sources of power supply whose voltage and frequency fluctuate considerably, and must be capable of being set up for operation in a very short time and of operating over long periods with a minimum amount of maintenance. These engineering problems were quickly and successfully solved and during 1942 some of the first military wire-line telegraph systems were delivered to the Army. Constant progress was made after that date and the armed forces were well supplied with up-to-date military d-c telegraph and wire-line carrier telegraph equipments for handling their teletypewriter and other telegraphic communications. The engineering problems were attacked and solved, having in mind that all of the components used in these military teletypewriter communication circuits had to fit into and be a part of a complete working system, each component forming a link in a chain. The approach is sometimes called the "system's viewpoint" and is the method which is followed in planning and engineering American *TWX* and private line teletypewriter networks.

Military D-C Telegraph Arrangements

In the military d-c telegraph field some of the important units, used by our armed forces to handle teletypewriter communications in the theatres of operation, made by various manufacturers, are:

*Teletypewriter Set EE-97-A**. This set includes a page-receiving and keyboard-sending teletypewriter, coded *TG-7-B*, which is arranged for operation at 60 or 66 words per minute. It also includes line unit *BE-77* and power equipment. Figure 1(a) shows teletypewriter set *EE-97-A* and Figure 1(b) is an illustration of how it was used over short wire-lines. It is used also in carrier telegraph and radio teletypewriter networks.

*Reperforator Teletypewriter Set TC-16**. This set includes reperforator transmitter *TG-26-A*, line unit *BE-77-A* and a power supply unit. Reperforator transmitter *TG-26-A*, perforates and types messages on a tape from a local keyboard or from line signals and sends signals from tape run through the transmitter at either 60 or 66 words per minute. Reperforator teletypewriter set *TC-16* was connected in the same general manner as teletypewriter set *EE-97-A* to d-c telegraph wire-line circuits, and to carrier and radio teletypewriter circuits. Figure 2 is an illustration of this set.

*Telegraph Central Office Set TC-3**. This set includes a 10-line teletypewriter switchboard, coded *BD-100*, a *TG-7-B* operator's teletypewriter and power supply equipment. The *BD-100* switchboard provides a means of interconnecting wire-line and radio teletypewriter circuits manually for either point-to-point or conference connections. Two or more *BD-100* switchboards were operated together to increase capacity. Figure 3(a) shows the telegraph central office set *TC-3* and Figure 3(b) is a schematic of a typical network including other tactical equipments.

Repeater TG-30, Repeater TG-31, and Repeater Package X-61824A. The *TG-30* and *TG-31* repeaters are combat types and the *X-61824A* package is a fixed plant type. All of these repeaters operate on a polarential[8] transmission basis. The *TG-30* and *X-61824A* provide means of operating teletypewriters over simplexed Army field wire up to 50 miles; by interposing a *TG-31* at the middle of such a circuit, operation up to 80 miles was possible. These repeaters could be used also on simplex and composite bases over cable and over much longer links of open wire. Figure 4 shows a *TG-30* and Figure 5 a *TG-31* repeater. Each cabinet includes a power rectifier for operation from a-c power. Figure 6 shows an *X-61824A* repeater package cabinet housing two repeaters with their associated power rectifiers.

Regenerative Repeater Package, X-66031A. This repeater provides a means of reconstructing teletypewriter signals after they have been badly distorted as a result of transmission over a telegraph circuit that is operated near its limit of transmission capability, thereby permitting a considerable extension of transmission range. Figure 7 shows a cabinet housing two of these repeaters.

Military Multichannel Carrier Telegraph Wire-Line Systems

The Army used two classes of multichannel carrier telegraph wire-line terminals in the theatres of operation, tactical and fixed plant. The tactical terminals, known as the *CF-2* and *CF-6* types, were used with the spiral-4 cable and also with Army field wire, with open-wire lines, and with commercial cable. The Army also used them with portable frequency-modulation radio sets to pro-

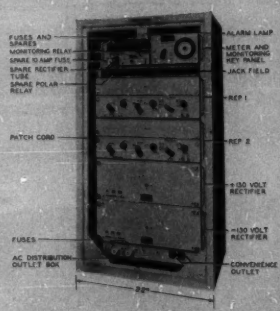


Figure 6. Telegraph repeater package, X-61824A (Terminal)

*These sets were developed by Signal Corps Laboratories. They were manufactured by various contractors and made use of teletypewriter apparatus furnished by Teletype Corporation.

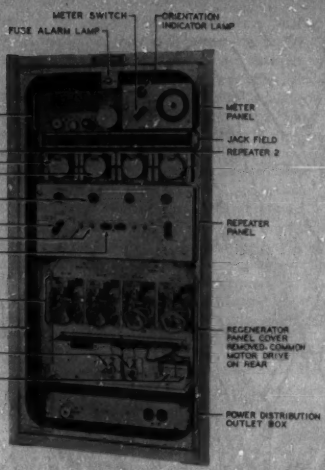


Figure 7 (above). Regenerative repeater package X-66031A

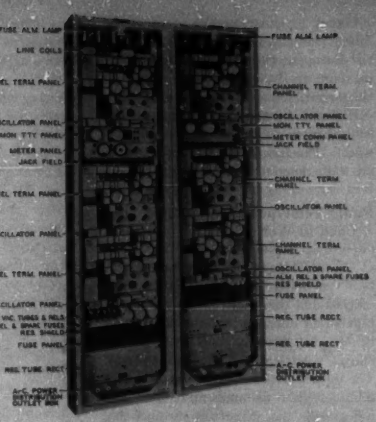
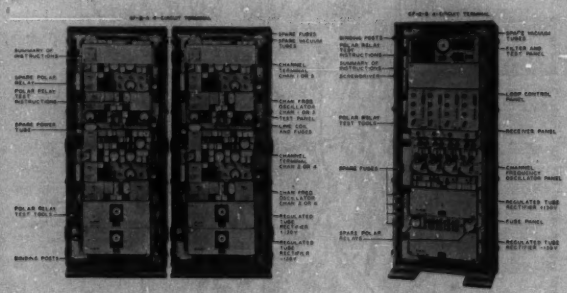


Figure 9. Fixed plant military voice frequency carrier telegraph terminal X-61822

Figure 8. Military voice frequency carrier telegraph terminals

View showing physical comparison



vide multichannel teletypewriter circuits. There are two types of *CF-2* terminals; these are coded *CF-2-A* and *CF-2-B*.[18] Either type provides four 2-way teletypewriter circuits over a 2-wire circuit that is suitable for telephone. By supplementing either of these terminals with *CF-6-A*, a total of six 2-way teletypewriter circuits was provided over a 2-wire circuit. Where 4-wire telephone facilities were available, the number of teletypewriter channels could be doubled by using twice as much terminal equipment. The *CF 2-A* terminal was superseded by the *CF-2-B*, which is one-half the size and is about 55 per cent as heavy. Figure 8 shows a comparison in size and appearance of the *CF-2-A* and *CF-2-B* terminals.

In addition to the *CF-2* and *CF-6* types of carrier telegraph terminals, a so-called "packaged carrier telegraph system terminal" was provided for fixed plant use by the Signal Corps. This equipment makes use of the same apparatus components as those used in the *CF-2-A* but the over-all assembly is quite different both physically and electrically, as the packaged equipment was called upon to meet somewhat different service requirements than the tactical equipment. The package terminal is arranged for operation over a 4-wire telephone circuit and either 6 or 12 2-way teletypewriter circuits could be obtained over such a circuit, depending upon the amount of equipment used. Figure 9 shows the complete equipment of a 6-circuit terminal. For 12 circuits an additional pair of cabinets was required.

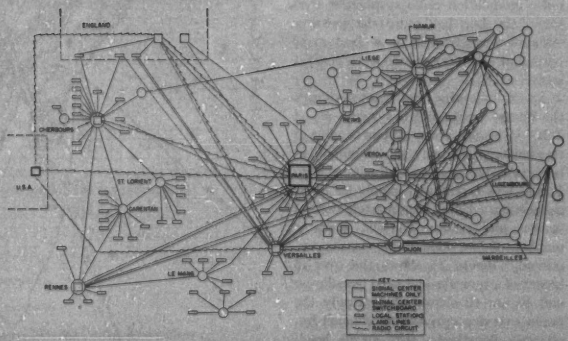


Figure 10. Teletypewriter networks in European theatre of operations, January 1945

Figure 10 illustrates the scope of the "fixed plant" teletypewriter networks in the European theatre of operations during January 1945; the tactical circuits in the forward areas are not included. The carrier and d-c telegraph equipments described previously were used in this network. The long-haul radio equipments are those described later in this paper.

Radio Teletypewriter Systems Used in World War II

The radio teletypewriter communication networks used by Government officials and the armed forces included a number of different systems which may be divided broadly into two general classes, short-haul and long-haul systems. The former class was generally used over relatively short distances up to about 200 miles with the maximum distance between radio repeater points not in excess of about 40 miles; the latter over long stretches of water or over difficult terrain where intermediate radio repeater points were impracticable. Each teletypewriter channel in these systems handled traffic at a normal rate of 60 words per minute and sometimes at higher speeds.

Short-Haul Radio Teletypewriter Systems

Short-haul radio teletypewriter systems that were developed by the Bell Laboratories during the war for the Army and Navy made use of telephone-type radio sets operated on either an amplitude or frequency modulation basis. Toward the end of the war it was also possible to establish radio teletypewriter communication links in the ultra high-frequency radio range by applying these short-haul radio teletypewriter systems to one or more of the channels in multichannel pulse modulation radio systems.[26] Certain of the short-haul radio teletypewriter systems were installed on a fixed plant basis and others were used on a combat basis.

An example of a fixed plant system is the Navy "carrier control system—model *UN*." This is a 1-way system capable of transmitting as many as 36 individual teletypewriter messages simultaneously. It was, and is still used mainly for interconnecting a naval headquarters control station and nearby stations where a number of long-haul radio circuits terminate. Figure 11 shows the over-all layout of a typical small sized installation and Figure 12 shows schematically the various service options and channel frequency allocations. The naval headquarters control station is located several

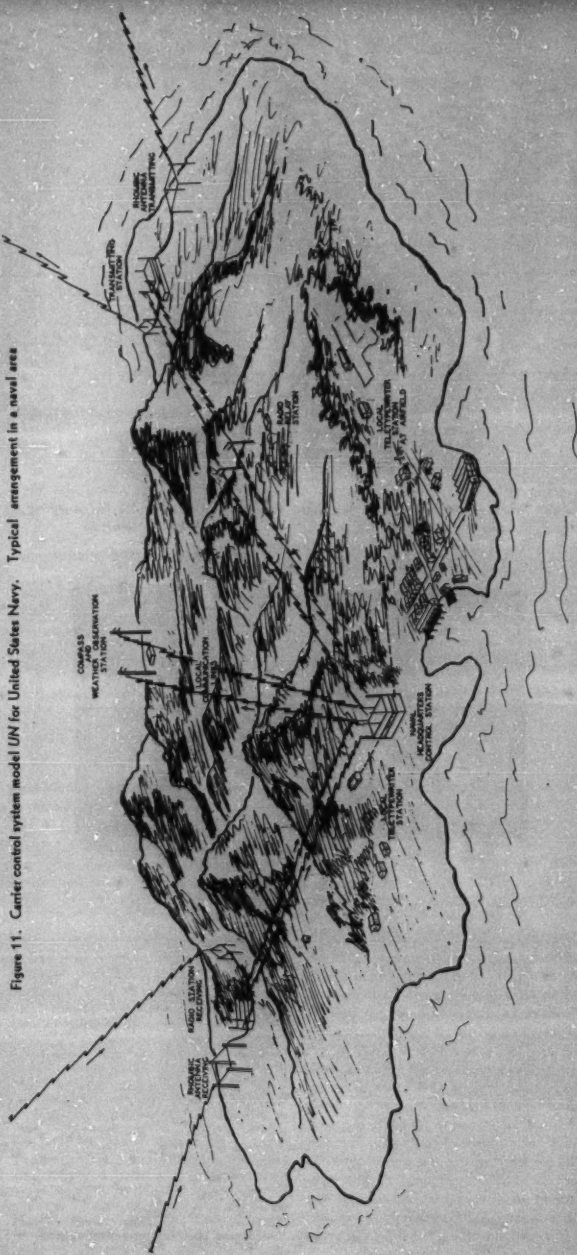


Figure 11. Carrier control system model *UN* for United States Navy. Typical arrangement in a naval area

miles from either of the long-haul radio stations which are separated from each other by several miles to avoid mutual interference. Originally intended as a fall-back system for emergency use in cases of failure of wire lines, the model *UN* system is being used satisfactorily in the Hawaiian Islands, and in the Washington, Panama, San Diego, San Francisco, Seattle, San Juan, and other areas in regular service. Bell Laboratories engineers designed the radio transmitters, receivers, and carrier equipment in this system, and also co-operated in the field in connection with their installation and adjustment. A somewhat similar system, coded *42B1*, was also developed for the Army. It came into use during the war to interconnect terminals of long-haul radio teletypewriter links and Army signal centers, such as between the radio station at Fort Myer, Va., and the telegraph central office—WAR—in Washington.

A portable short-haul multichannel teletypewriter system, which is known as the Navy "portable carrier control system—model *UF*," provides four teletypewriter channels in each direction over a 2-way radio link. The equipment may be landed readily on a beach and requires only a few minutes to set up for service. Figure 13 shows a schematic of a typical setup of the equipment of the model *UF* system. The line terminals and radio line terminal, shown in Figure 13, were ordinarily used in conjunction with a portable amplitude or frequency modulation *VHF*-type radio set for termination of a point-to-point multichannel radio teletypewriter circuit. The capacity of the system is four 2-way teletypewriter circuits, each operating at 60 words per minute.

Long-Haul Radio Teletypewriter Systems

The transmission of teletypewriter pulses over long-haul high-frequency radio was not accomplished, as might be supposed, merely by connecting the teletypewriter machines to radio facilities that were in use prior to the war. The prewar radio systems did not contain the features to overcome the effects of signal fading and other natural and man-made disturbances required for good teletypewriter service. The wide experience of Bell Telephone System engineers in the design of electronic circuits and in the development of land-line teletypewriter and radio telephone systems proved to be of great value in their efforts to surmount these transmission difficulties. With the co-operation of the Army and the Navy, the required features were developed during the early part of the war and applied to long-haul high-frequency radio to permit teletypewriter machines to receive enciphered radio teletypewriter messages with only an occasional error.

In the conventional single-channel long-haul radio telegraph system in general use before the war, the radio transmitter was keyed by manual or automatic methods using the international Morse code. With this method, the sending key controls the output of the radio transmitter; when the key is closed energy at a particular radio frequency is transmitted and when the key is open the transmitter is cut off. Reception of these signals is possible using a tunable amplitude-modulation radio receiver similar to those in homes. The listener reads the interrupted "tone" signals as "dots" or "dashes," or they are automatically recorded on a tape.

If the transmitting telegraph key is replaced by the sending circuit of a teletypewriter and a simple rectifier is interposed between the output of the radio receiver and the receiving teletypewriter, it is possible under favorable conditions to receive good teletypewriter copy. Frequently, however, the arrangement will be unsuitable.

In the single-channel long-haul *AN/FGC-1* system, shown in Figure 14 and developed during 1942,[22] a new device, known as a frequency shift keyer, is actuated by the open and close pulses produced by the sending teletypewriter apparatus. The keyer causes the radio transmitter to emit radio power at a particular frequency for teletypewriter open pulses and a slightly different frequency but the same power for the close pulses. In order to minimize some of the transmission difficulties experienced in many long-haul radio paths, such as the tendency for a radio wave to fade out at a particular spot but to be noted at another point close by, two separate antennas and two radio receivers are used, as shown in Figure 14. This is known as space diver-

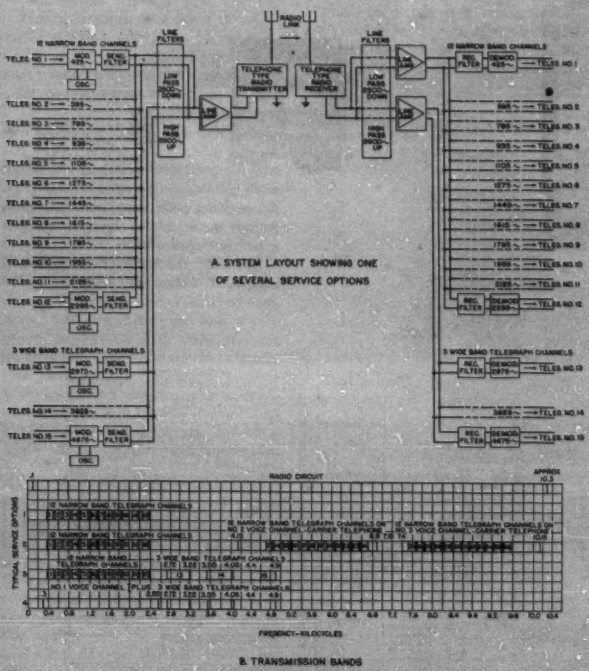
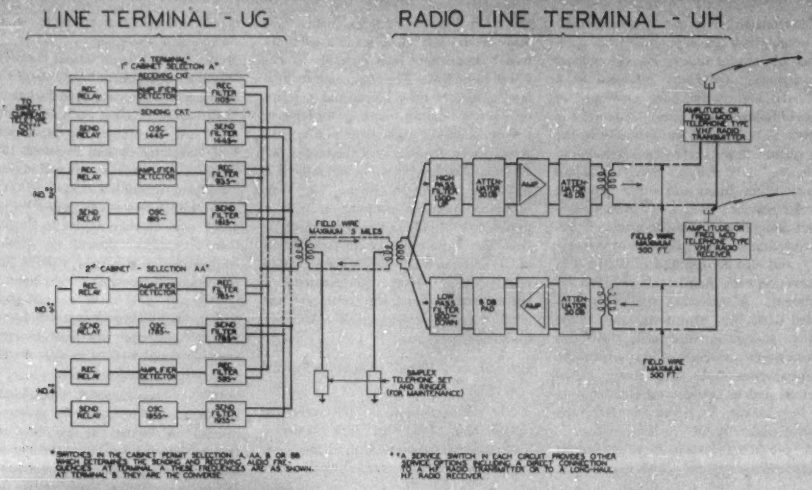
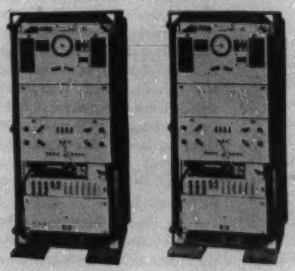


Figure 12. Carrier control system, model *UN*, for United States Navy

Typical system layout and transmission bands





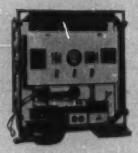


Figure 13. Carrier control system, model *UF*, for United States Navy

sity reception and, with antennas spaced a few wave lengths apart, fading out of the received signals is extremely unlikely to occur at both receiving points at the same time.[17]

Except during fading, when one radio receiver may become virtually inactive, each receiver delivers to the teletypewriter system a current of a particular audio-frequency when a teletypewriter open pulse is received and a current of a different audio-frequency when a teletypewriter close pulse is received.

Each channel of the teletypewriter terminal equipment includes a wide-band filter, a fast-acting current limiter, the input-output characteristic of which is shown in Figure 15, a filter and a detector *S* for the reception of teletypewriter open signals, and similar equipment for the reception of close signals. The output circuits of the two channels in the terminal will appropriately combine these signals to operate a polar relay, which, in turn, delivers d-c signals to the receiving teletypewriter apparatus.

By having the limiters in the receiving equipment continuously energized by the

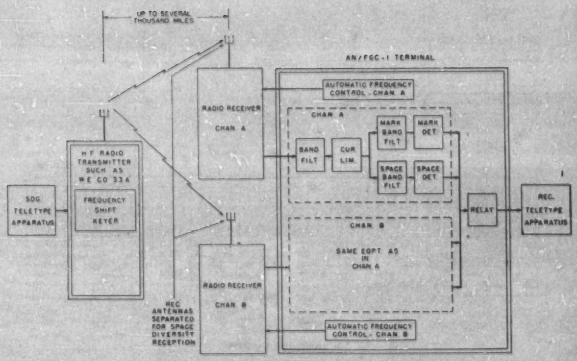


Figure 14. Single channel frequency shift long-haul radio teletypewriter link using *AN/FGC-1* terminal equipment

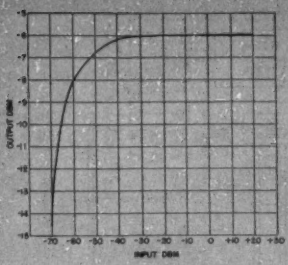


Figure 15. Current limiter characteristic

frequency shift constant power radio transmitter, the effects of noise are overcome to a much greater degree than in the on-off keying method. When combined with space diversity reception the resultant system was found to be highly reliable. Often, when the tones are indistinguishable to the ear because of noise, the receiving teletypewriter continues to operate without errors.

The equipments comprising the *AN/FGC-1* system are arranged for rapid installation and, although somewhat large, were transported by airplane into advance points to meet urgent needs.

An alternative, but quite similar system to the one developed for the Army, and known as the *AN/FGC-1A* was designed and made available for use by the Navy at their large well-established shore station installations. Many of these are now being operated over routes such as Washington—Honolulu, San Francisco—Honolulu and Washington—Balboa.

In addition to the need for the single-channel long-haul *AN/FGC-1* system, during 1942 the Army also had a very urgent need for a high traffic-carrying capacity long-haul radio system to handle their written communication between Washington and major overseas headquarters. To care for this demand Bell Telephone System engineers devised a highly satisfactory long-haul multichannel radio teletypewriter system[29] making use of available single side-band twin-channel radio telephone[29,30] and carrier telegraph equipments together with additional components, such as fast-acting current limiters (Figure 15), that were needed to make the system suitable for reception of teletypewriter signals. The radio transmitters and receivers used in this system are the suppressed-carrier twin-sideband types that have been in use during the last several years in Bell Telephone System overseas telephone service. The multichannel carrier telegraph equipment is an adaptation of the type used extensively in Bell Telephone System carrier telegraph wire networks. By applying the carrier telegraph multichannel frequencies to one sideband of this system in such manner that two separate and distinct audio-frequency tones are supplied for each teletypewriter channel during the close-circuit condition and two other tones during the open-circuit condition, frequency diversity operation is provided. The use of two frequencies for each signal condition minimizes the effects of signal fading in a manner similar to space diversity operation described previously. Figure 16 shows schematically one terminal of this system.

The first of these systems was provided within a few weeks after it was requested and successful operation was obtained between Washington and London using one sideband of the existing radio telephone system to provide three simultaneous teletypewriter circuits in each direction. The capacity of this system was later doubled. The Army operated a number of these multichannel transoceanic links for many months in a highly successful manner between such points as Washington—London and San Francisco—Australia. The Navy obtained also a number of these systems for use over their main global communication routes, such as that between Washington and Honolulu. A common military application of this system included six telegraph channels applied to one of the sidebands of the twin-channel radio telephone system with the other sideband available for telephone. When used in this manner the system had a teletypewriter traffic-carrying capacity of about 5,000 100-word messages per day in each direction.

As a result of the phenomenal success of the *AN/FGC* type systems from the outset, both the Army and Navy became very much interested in obtaining smaller and lighter weight frequency shift keying systems. Small size equipments were particularly important to both services in connection with their contemplated large-scale use of long-haul radio teletypewriter circuits between points in rear areas and advanced bases and between shore stations and ships. As a result of this interest, during 1944 the Bell Telephone Laboratories undertook the design of smaller equipments. One of these was a new receiving equipment for use with a pair of amplitude-modulation radio receivers, designated Navy "model *FRF*

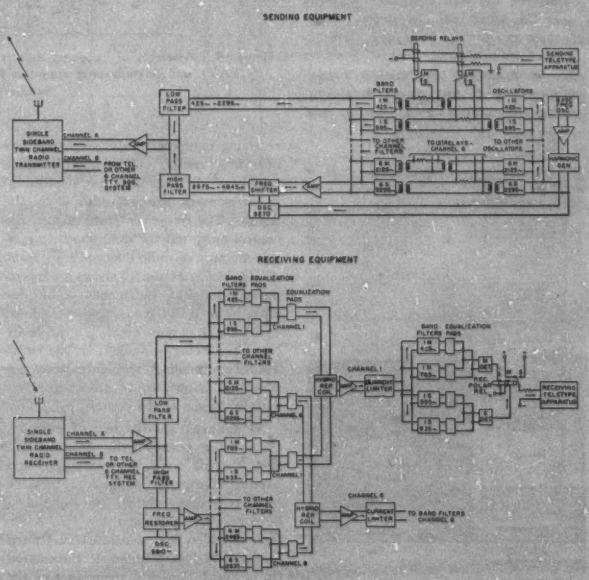


Figure 16. Multichannel radio teletypewriter system or single sideband twin-channel radiotelephone

frequency shift receiver converter equipment."

Field tests made by the Navy of an engineering model of this new equipment indicated that it performed better than the *AN/FGC* type though much smaller and lighter. More recently a large number of these equipments have been delivered to the Navy for use at both shore and ship stations. Figure 17 shows a physical comparison between the radio telegraph terminal equipment, *AN/FGC-1*, and the model *FRF* frequency shift receiver converter equipment.

The Bell Telephone Laboratories also developed frequency shift applique equipment for use with the truck-borne radio set *SCR-399-A* which was the Army's tactical work-horse among radio sets. This applique, designated radio teletypewriter equipment *AN/TRA-7*, permits the radio equipment to send teletypewriter signals on a frequency shift basis and to receive these signals on a dual space diversity basis. In addition to permitting 2-way or full duplex teletypewriter service, the *AN/TRA-7* equipment contains features to permit a new form of service, known as one-way reversible. With this service a to-and-fro teletypewriter communication can be carried on between two distant points using the same radio frequency assignment on an alternate basis for both directions of transmission. Figure 18 shows the arrangement of the *AN/TRA-7* equipment and other components comprising the complete radio sending and receiving equipments and teletypewriter operating center. This complete assembly of three shelters has been designated as radio set *AN/MRC-2* by the Army. The frequency shift exciter, the control unit, and the dual diversity converter comprise radio teletype equipment *AN/TRA-7*. Figure 19 shows an experimental model of the frequency shift exciter. The converter equipment, shown in Figure 17, is quite similar to the Army converter being provided in the *AN/TRA-7* equipment. The control unit, not shown, is housed in a cabinet approximately the size of the exciter.

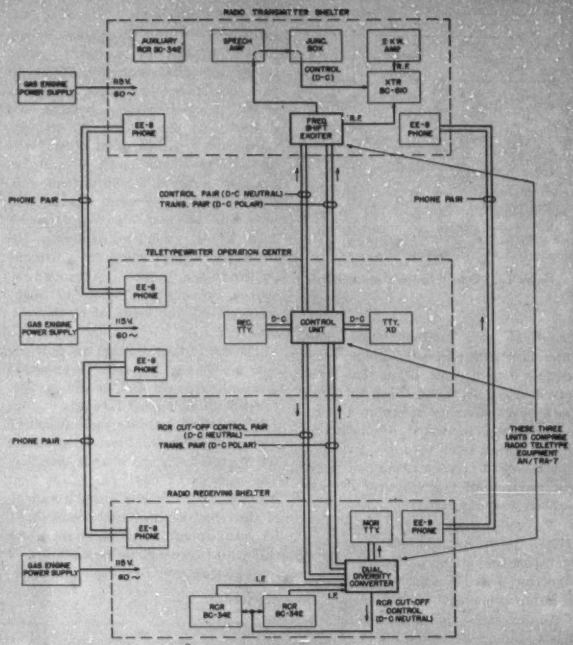


Figure 18. Assembly of frequency shift radio equipment (radio teletypewriter equipment *AN/TRA-7*) with teletypewriter apparatus and radio set *SCR-399-A* to provide signal corps radio set *AN/MRC-2*

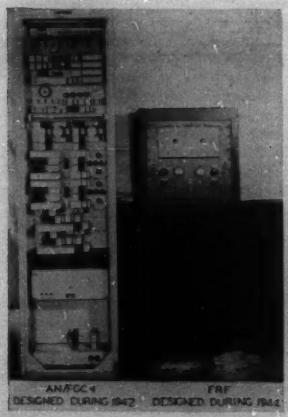


Figure 17. Single channel dual space diversity radio teletypewriter receiving equipments

As indicated on the map, Figure 20, these radio teletypewriter communication systems were very extensive and handled a large part of the total message traffic over many routes. For example, a large network, using the *AN/FGC-1* system, extended from southeastern United States to the West Indies, to the eastern tip of South America, thence across the Atlantic to Africa. Others linked up India, China, New Guinea, and Australia and numerous islands in the Pacific Ocean, Honolulu, and the west coast of the United States. Another *AN/FGC-1* system extended northward and eastward to Newfoundland, Labrador, Greenland, Iceland, the Azores, the United Kingdom, Russia, and Italy.

Looking at the matter broadly, it may be said that the long-haul radio teletype-

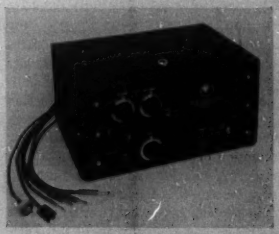


Figure 19. Frequency shift exciter, O-39( )-TRA-7 for use with radio set *SCR-399-A*

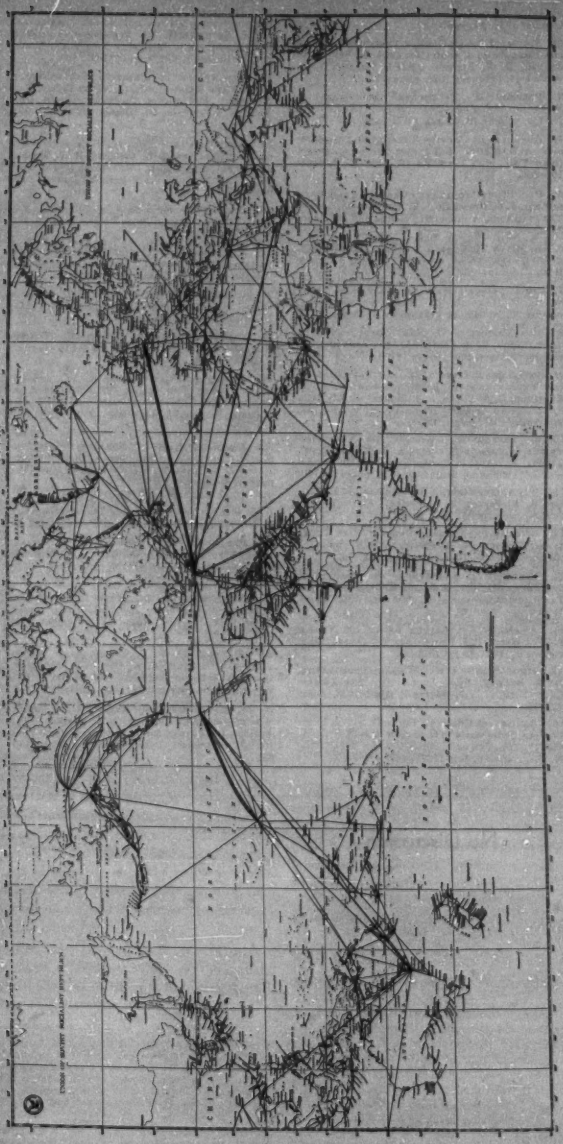


Figure 20. Long-haul military radio teletypewriter circuits during 1944

Legend: ——Multichannel System

——Frequency Shift Single Channel System

writer systems provided outstandingly reliable, rapid and secret communication facilities which were found by the armed forces and Government officials to be much better suited for providing extensive military communication networks than alternative arrangements. Although each teletypewriter channel usually operated at a rate of only 60 words per minute, whereas some automatic Morse channels operate at much higher speeds, experience demonstrated that as much or more traffic could be handled each day over a single teletypewriter channel as over a single high-speed automatic Morse circuit. This is possible because the new method of transmission is so superior that the teletypewriter traffic flows at a uniform rate hour after hour with few if any interruptions and enciphering and deciphering may be automatic and instantaneous. This is in contrast to Morse methods which require interpreting and typing the received messages.

The successful application of teletypewriters to long-haul radio links permitted the establishment and successful operation of extensive military teletypewriter networks. This is because the long-haul radio links could be operated in the same manner as the short-haul radio links and wire-line circuits with which they were associated. Thus, by the help of teletypewriter tape relay methods, in which messages were stored in perforated tape form and these used to control transmitters on other circuits, teletypewriter networks extending over land and sea and including long-haul radio facilities and American private-line networks, were operated as an integrated military communication system including connections, as required, with certain stations of the *TWX* system. Results were successful far beyond previous accomplishments in overseas transmission of written communications via radio or other means, and perhaps beyond expectations. Favorable and enthusiastic reports were received on the use of these facilities. Our military leaders and government officials depended upon them for communications, including scheduled conferences in which groups separated by thousands of miles passed communications back and forth practically instantaneously.

The following quotations from President Roosevelt's speech to the Congress on March 1, 1945, regarding the Yalta conference indicates the importance of radiotelegraph in handling the communications while en route to and from Yalta and during the conference there. Both Morse and teletypewriter methods were employed.

"Far away as I was, I was kept constantly informed of affairs in the United States. The modern miracle of rapid communication has made this world very small; and we must always bear that in mind, when we think or speak of international relations. I received a steady stream of messages from Washington, and except where radio silence was necessary for security purposes, I could continuously send messages any place in the world. And, of course, in a grave emergency, we could even have risked breaking the security rule."

Future Outlook

Radio teletypewriter methods not only provide direct reading, no delay, point-to-point overseas communications, but also make possible the radio broadcasting of information from a single station to many others and for conferences to be held, including two or more groups of conferees located on various continents and at sea, all communications being on a secret basis. Radio provides means for communication to moving units, and a new radio teletypewriter service to motor cars, airplanes, and other similar vehicles, using special radio transmission features to secure dependability, now is also technically possible. It would therefore seem certain that the radio teletypewriter communication methods developed during the war will be of prime importance in the future because of their efficient use of the radio spectrum, now so restricted, and the advantages of secrecy, speed, and reliability.

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No Discussion

Reactance of Large Cables in Steel Pipe or Conduit

WILLIAM A. DEL MAR
FELLOW AIEE

Synopsis: The determination of reactance of large conductors in steel pipes or conduits has not been susceptible of practical computation but is required for estimating the voltage regulation of pipe cable systems. The present paper gives the results of tests made by Electrical Testing Laboratories (ETL) under the auspices of the Insulated Power Cable Engineers Association, on cables from 750,000 to 2,500,000 circular mils. These data have been supplemented by data from other sources, extending the lower limit of size to 4/10 American wire gauge. Empirical formulas have been developed to include all intermediate sizes and spacings. These formulas are based on the principle of deriving a self-GMD (geometric mean distance) from the a-c/d-c resistance ratio, and using an augmented axial spacing to allow for the magnetic effect of the pipe.

THIS is a companion paper to those entitled A-C Resistance of Large Size Conductors in Steel Pipe or Conduit[1] and Problems in the Measurement of A-C Resistance and Reactance of Large Conductors.[2] It relates to data on reactance, that is, positive sequence reactance, obtained simultaneously with those of the former paper and by procedure described in the latter one.

The tests covered three sizes of cable, 750,000, 1,500,000, 2,500,000 circular mils, the last of these with both concentric and segmental stranding.

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WILLIAM A. DEL MAR is chief engineer with the Habirshaw Cable and Wire Division of the Phelps Dodge Copper Products Corporation, Yonkers, N. Y.

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The stranding of the concentric cables was as follows: 750,000—61 wires; 1,500,000—91 wires; 2,500,000—127 wires.

The reactance data obtained were as follows, all in ohms per 1,000 feet at 60 cycles and applying to one conductor to neutral, the average for the three conductors being given:

Conditions	Table Number
Three conductors in triangular formation in air	I
Three conductors in flat arrangement in air	II
Three conductors in triangular formation in steel pipe	III
Three conductors in triangular formation in steel conduit	IV
Three conductors in flat arrangement in steel conduit	V

Three Conductors in Triangular Formation in Air: Table I, relating to cables assembled in triangular arrangement in air, will be considered first in relation to the formula ordinarily used, which is as follows:

$$X = 0.0529 \log_{10} \frac{D}{\text{GMD}} \qquad (1)$$

where

X = reactance (60 cycles) in ohms per 1,000 feet
D = axial separation of conductors, inches
GMD = self geometric mean distance of the conductor, which would have the following values for the concentrically stranded conductors:
750,000 circular mils, 0.3853
1,500,000 circular mils, 0.5468
2,500,000 circular mils, 0.7090

This formula does not take into account either skin effect or proximity effect, which latter results from the currents in adjacent phases drawing the current centers inward in relation to the conductor centers and thereby reducing the reactance.

Gordon Thompson has suggested a modification of equation 1 which takes into account the skin effect and assumes the proximity effect to be an accentuation of skin effect without axial displacement of the magnetic field. The actual conductor is assumed to be replaced by an annulus of equal outside diameter D_o and of an internal diameter D_i, such that the d-c resistance of the annulus is equal to the a-c resistance of the actual conductor. Then the cross-section of the annulus (copper plus voids) will be

$$\frac{\pi}{4}(D_o^2 - D_i^2)$$

and that of the actual conductor, (copper plus voids)

$$\frac{\pi}{4} D_o^2$$

Their ratio

$$\frac{D_o^2}{D_o^2 - D_i^2}$$

assuming equal proportion of copper and

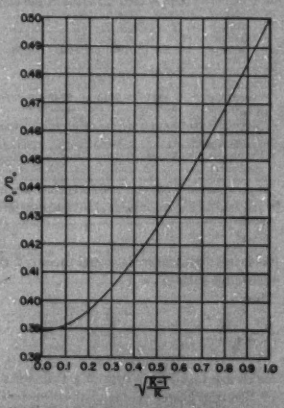

Figure 1. Ratio of self-GMD (geometric mean distance) to outside diameter of annulus in terms of a-c-d-c resistance ratio K

Table I. Reactance of Three Conductors in Triangular Arrangement in Air

Size, Circular Mils	Spacing Center to Center in Inches: 1.324		1.598		2.198		1.70		2.17		2.07	
	Copper Temperature, Degrees Centigrade: 20	70	20	70	20	70	20	70	20	70	20	70
750,000 concentric	0.0268	0.0270	0.0297	0.0314	0.0416	0.0414						
1,500,000 concentric							0.0240	0.0245				
2,500,000 concentric									0.0228	0.0228		
2,500,000 segmental											0.0251	0.0254

Brieger obtained 0.0292 for 500,000 circular mil cables on 1.23 inch spacing

Note that in all cases except one the 20 and 70 degree figures are within less than 2 per cent whereas the values for 750,000 on 1.598 spacing differ by about 6 per cent. One of them, probably, the 20-degree figure is in error, and it will be omitted from subsequent tables, and the 70-degree figure used.

Table II. Mean Reactance of Three Conductors in Flat Arrangement in Air

Circular Mils	A		B	
	20°C	70°C	20°C	70°C
750,000	0.0318	0.0334	0.0354	0.0354
2,500,000			0.0271	0.0272

A. Cradled as if in 4-inch conduit, 1.324-inch axial spacing.

B. Cradled as if in 6-inch conduit, the 750,000 circular mil cable on 1.324-inch axial spacing and the 2,500,000 circular mil cable on 2.17-inch axial spacing.

Table III. Reactance of Three Conductors in Triangular Arrangement in 6-Inch Pipe

Size, Circular Mils	Spacing Centers, Inches: 1.598		2.198	
	Copper Temperatures: 20°C	70°C	20°C	70°C
750,000 concentric	0.0363	0.0366	0.0458	0.0468

An equivalent curve is given herein as Figure 1. This curve is based on solid annuli, but is nearly correct for stranded. Taking the experimentally determined value of K and deriving

$$\sqrt{\frac{K-1}{K}}$$

the ratio D_s/D_o is read from the curve and D_s derived by multiplying by D_o.

Then

$$X = 0.0529 \log_{10} \frac{D}{D_s} \qquad (2)$$

The reactance calculated by equations 1 and 2 are shown in Table VI in comparison with the measured values. The ratios of the measured to the calculated values are shown in Table VII from which it appears that equation 2 gives better agreement with the measured values. It will be noted also that the values obtained by equation 2 are always less than those obtained by equation 1, which is as it should be, as an annulus must have a lower reactance than a solid conductor of equal diameter.

Three Conductors in Triangular Formation in Steel Pipe or Conduit. The data for 6-inch steel pipe are given in Table III, and for 4-inch to 6-inch conduit in Table IV.

The ETL measurements were made on only three sizes of cable but fortunately, equivalent data were obtained for two other sizes through the kindness of Lawrence Brieger of the Consolidated Edison Company of New York, Inc., and these are studied in connection with the ETL data, giving much better basis for generalizations.

Due to the limited number of tests with different pipe or conduit sizes, no clear picture could be obtained of the influence of pipe size on the reactance. It was, therefore, decided to neglect pipe diameter and find some relation involving only the conductor size and spacing.

Two methods of generalizing these data were tried. One was to obtain self-GMD values for use in equation 2; the other, suggested by E. H. Salter,[9] was to use an empirical increase of spacing in the same equation using the values of K, in air. Mathematically, these two methods would voids in both cases, must equal the a-c/d-c resistance ratio K.

Then

$$\frac{D_i}{D_o} = \sqrt{\frac{K-1}{K}}$$

Referring to L. F. Woodruff's Principles of Electric Power Transmission,[8] a formula and curve are given from which the self-GMD of the annulus, D_s, may be obtained in terms of the foregoing ratio.

Table IV. Reactance of Three Conductors in Triangular Arrangement in Conduit

Size, Circular Mils	Spacing Center to Center in Inches: 1.324		1.324		1.598		1.598		2.198		1.70		2.07		2.17	
	Conduit Diameter in Inches: 4		5*		5		6		6		5		6		6	
	Copper Temperature, Degrees Centigrade: 20	70	20	70	20	70	20	70	20	70	20	70	20	70	20	70
750,000 concentric	0.0331	0.0343	0.0349	0.0372	0.0372	0.0377	0.0370	0.0376	0.0469	0.0468						
1,500,000 concentric											0.0275	0.0286				
2,500,000 concentric															0.0254	0.0258
2,500,000 segmental													0.0291	0.0298		

Brieger obtained 0.0348 for 300,000 circular mils in 4-inch iron conduit and 1.23-inch in spacing.

* This case is exceptional in the great difference between the 20- and 70-degree data and its correctness may be doubted. It is, therefore, omitted from subsequent tables.

Table V. Reactance of Three Conductors in Conduit in Flat Arrangement

Size, Circular Mils	Conduit Diameter, Inches: 4		6	
	Copper Temperature: 20°C	70°C	20°C	70°C
750,000 concentric	0.0408	0.0410		
2,500,000 concentric			0.0298	0.0302

Brieger obtained 0.0468 for 500,000 circular mils in 4-inch iron conduit on 1.23-inch spacing.

Table VI. Comparison of Measured and Calculated Reactances for Conductors in Triangular Formation in Air

Conductor Cross-Section, Circular Mils	Axial Spacing, Inches	Ratio R_{ac}/R_{dc} K*	Reactance, Measured Average of 20°C and 70°C	Ohms Per 1,000 Feet, Calculated Equation 1	Equation 2
750,000	1.324	1.157	0.0268	0.0290	0.0269
750,000	1.598	1.095	0.0314	0.0327	0.0316
750,000	2.198	1.073	0.0415	0.0400	0.0391
1,500,000	1.70	1.513	0.0242	0.0261	0.0234
2,500,000	2.17	1.934	0.0228	0.0258	0.0222

* These are the average of measured values obtained by ETL at 20 and 70 degrees centigrade. They are here carried to one more decimal place than in Wiseman's paper.[1]

appear to be identical, as they occur as a ratio in equation 2. The latter method, however, was found to be both more practical and more universal in its application, as it works equally well with concentric and segmental strandings.

In order to find the empirical increase in spacing, the necessary data are assembled in Table VIII.

This shows, for each case, the conductor diameter D_o, the actual axial spacing D, the a-c/d-c resistance ratio K, and the derived ratio

$$\sqrt{\frac{K-1}{K}}$$

Using Figure 1, the corresponding values of D_s/D_o for an equivalent annulus are taken from the curve. The self-GMD of the equivalent annulus, D_s, is derived from $D/{}_sD_o$ by multiplying by D_o. This is then put in equation 2, with the measured reactance in pipe or conduit and the empirical equivalent spacing D_1 calculated. This spacing, on the average, is 1.27 times the actual spacing. In view of the range in this ratio, we are not justified in using three places of decimals and a ratio of 1.3 will be used.

The reactance of cables in pipe or conduit would, therefore, be obtained by using the a-c/d-c resistance ratio in air, K, deriving

$$\sqrt{\frac{K-1}{K}}$$

and obtaining the corresponding ratio D_s/D_o from Figure 1. D_s would be obtained by multiplying this ratio by D_o, the cable diameter. The equivalent empirical spacing then would be $D_1 = 1.3\,D$ and the reactance would be given by

$$X = 0.0529 \log_{10} \frac{D_1}{D_s}$$

In Table IX, this factor of 1.3 is multiplied by the actual axial spacings and used in equation 2, to derive a value of X. This derived value is shown in the table after the measured value and their ratio is given in the last column. It will be seen that the error does not exceed 5 per cent except for the two extreme sizes, where it is 8 and 10 per cent respectively.

Segmental Cable: The only ETL data on segmental cable were obtained on one size, 2,500,000 circular mils and one spacing 2.07. However, the author was able to contribute data on a 1,500,000-circular-mil cable on 2.90 inch spacing.

The reactance, in both these cases, conforms with the "1.3 formula" used for concentric cable, as shown in Table VIII, cases 10 and 12.

Three Conductors in "Flat Arrangement" in Air: In this arrangement, the conductors were not actually flat but cradled so as to be in the same position as they would be when as flat as possible in a pipe or conduit of the stated size. The rather meagre data are given in Table II and their relation to the values for triangular arrangement in Table X.

It will be noted that in three out of six cases, the ratio of mean reactance for flat arrangement is from 1.19 to 1.21 times that for triangular arrangement. The one divergent case, where the ratio is 1.24, may be due to an error in the 70 degree measurement as this is 5 per cent over the 20 degree value, whereas in all the other cases the measurements at the

Table VII. Ratio of Measured to Calculated Reactances for Triangular Formation in Air

Conductor Circular Mils	Axial Spacing, Inches	Ratio of Measured to Calculated X Equation 1	Equation 2
750,000	1.324	0.093	1.00
	1.598	0.096*	0.99
	2.198	1.04	1.06
1,500,000	1.70	0.93	1.03
2,500,000	2.17	0.88	1.03
Average		0.94	1.02

* See footnote to Table I.

Table VIII. Equivalent Spacings for Cable in Pipe or Conduit (Triangular Arrangement)

Case	Circular Mils	Stranding	Diameter, Inch, D_o*	Actual Axial Spacing D	K**	$\sqrt{\frac{K-1}{K}}$	$\frac{D_s}{D_o}$	D_s	X in pipe or Conduit†	D_1	D_1/D
1	211,600		0.528	0.90	1.03	0.170	0.394	0.208	0.0364	1.014	1.13
2	500,000		0.814	1.23	1.08	0.272	0.402	0.337	0.0348	1.488	1.21
3	750,000		0.998	1.598	1.095	0.295	0.404	0.403	0.0364	1.965	1.23
4	750,000		0.998	2.198	1.073	0.261	0.401	0.400	0.0463	3.001	1.37
5	750,000	Concentric	0.998	1.324	1.157	0.368	0.411	0.410	0.0337	1.777	1.34
6	750,000		0.998	1.598	1.095	0.295	0.404	0.403	0.0374	2.023	1.27
7	750,000		0.998	1.598	1.095	0.295	0.404	0.403	0.0373	2.044	1.28
8	750,000		0.998	2.198	1.073	0.261	0.401	0.400	0.0468	3.067	1.40
9	1,500,000		1.412	1.70	1.513	0.582	0.436	0.616	0.0280	2.084	1.23
10	1,500,000	Segmental	1.377	2.90	1.160	0.371	0.411	0.566	0.0424	3.583	1.24
11	2,500,000	Concentric	1.824	2.17	1.934	0.695	0.453	0.824	0.0256	2.511	1.16
12	2,500,000	Segmental	1.747	2.07	1.493	0.575	0.436	0.762	0.0294	2.736	1.32

* Nominal diameters were used where actual diameter measurements were not available.

** K for items 1 and 2, estimated; taken from ETL measurements for all other items, including number 10.

† Items 3 to 9 inclusive and 11 and 12 from Tables III and IV, using the average of the 20 and 70 degree values. Items 1 and 2 from data by Brieger. Item 10 furnished by author from special ETL tests.

Table IX. Comparison of Measured and Calculated Reactance of Cable in Pipe or Conduit

Case	Circular Mils	Original Spacings, Inches	Original Spacing × 1.3	Self-GMD	Reactance × Calculated	Measured	Ratio Measured Calculated
1	211,600	0.90	1.170	0.208	0.0397	0.0364	0.92
2	500,000	1.23	1.599	0.327	0.0364	0.0348	0.96
3	750,000	1.598	2.077	0.403	0.0377	0.0364	0.97
4	750,000	2.198	2.857	0.400	0.0452	0.0463	1.02
5	750,000	1.324	1.721	0.410	0.0330	0.0337	1.02
6	750,000	1.598	2.077	0.403	0.0377	0.0374	0.99
7	750,000	1.598	2.077	0.403	0.0377	0.0373	0.99
8	750,000	2.198	2.857	0.400	0.0452	0.0468	1.04
9	1,500,000	concentric	1.70	2.210	0.616	0.0294	0.0280	0.95
10	1,500,000	segmental	2.90	3.77	0.566	0.0436	0.0424	0.97
11	2,500,000	concentric	2.17	2.721	0.824	0.0283	0.0256	0.90
12	2,500,000	segmental	2.07	2.69	0.762	0.0313	0.0294	0.94
	Average							0.97

Table X. Comparison of Flat and Triangular Arrangements in Air*

Arrangement	Ohms per 1,000 Feet 20°C	Ohms per 1,000 Feet 70°C	Cradled as in Conduit of Diameter Inches
750,000 Circular Mils, 1.324-Inch Axial Spacing			
Flat	0.0318	0.0334	4
Triangular	0.0265	0.0270	4
Ratio flat/triangular	1.19	1.24	
Flat	0.0354	0.0354	6
Triangular	0.0268	0.0270	6
Ratio flat/triangular	1.21	1.20	
2,500,000 Circular Mils 2.17-Inch Axial Spacing			
Flat	0.0271	0.0272	
Triangular	0.0228	0.0228	
Ratio flat/triangular	1.19	1.19	

* Tests reported by Brieger on 500,000 circular mil cable give a ratio of 1.19.

two temperatures are substantially identical.

As the ETL tests covered 750,000 and 2,500,000-circular-mil sizes, we may adopt as a working rule, that the average reactance in air, with flat arrangement is 19 or 20 per cent over that in air with triangular arrangement. The reactance in "flat arrangement" alternatively, may be computed, as for a triangular arrangement, using the cube root of the product of the three axial distances. This, however, involves the computation of the major axial distances as the cables lie in the pipe.

Three Conductors in Flat Arrangement in Conduit: Table V gives the available data and Table XI, a comparison of reactances of cables in flat and triangular arrangements. As with cables in air, the average increase of reactance due to the flat arrangement is 19 per cent, but the range is somewhat greater. The

Table XI. Comparison of Flat and Triangular Arrangements in Conduit

Arrangement	Ohms Per 1,000 Feet 20°C	Ohms Per 1,000 Feet 70°C
750,000 Circular Mils, 4 Inch Conduit		
Flat	0.0408	0.0410
Triangular	0.0331	0.0348
Ratio flat/triangular	1.23	1.20
2,500,000 Circular Mils 6 Inch Conduit		
Flat	0.0298	0.0302
Triangular	0.0254	0.0258
Ratio flat/triangular	1.17	1.17

cube root rule given previously applies less accurately to cables in conduit.

General Conclusions: The reactance of three conductors in a magnetic pipe is not capable of accurate determination because the spacing of the conductors and their positions with respect to the pipe are indeterminate and vary along their length. The diameter, the thickness, and permeability of the pipe also are variable. Nevertheless it is necessary to estimate the reactance in order to determine more or less approximately the voltage regulation of the line.

No theoretical method has been developed and no experimental data for large cables have been available.

The present work, while admittedly incomplete, at least gives us what we had hitherto lacked, a general idea of the magnitudes involved. Tests made by Brieger indicate that the random arrangement which occurs in the field gives reactances between those obtained in the laboratory for triangular and flat arrangements, but nearer to the latter, assuming that, in the laboratory tests, the outsides of the cables were touching along their entire length while, in the field, their spacing was random within the limits of the conduit.

The reactance of an actual 3-phase line in pipe or conduit would be calculated as follows:

1. Obtain the a-c/d-c ratio, K, for cables in air for the proper spacing, as given, for example, in Wiseman's paper.[1]

2. Find D_s the self-GMD for the corresponding value of

$$\sqrt{\frac{K-1}{K}}$$

in air, from Figure 1.

3. Obtain the equivalent spacing D_1 by multiplying the actual center-to-center spacing by 1.3.

4. Then X, the reactance in ohms per 1,000 feet for triangular arrangement of cable in pipe, will be

$$X = 0.053 \log_{10} \frac{D_1}{D_s}$$

5. If the cables are not in triangular formation increase this value of X by 15 per cent to allow for the random position of the cables in the pipe. This percentage is derived from the 20 per cent allowance for flat arrangement and Brieger's correction cited previously.

It is hoped that those who have made actual measurements on pipe lines will contribute their data to check this procedure.

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Discussion

L. Brieger (Consolidated Edison Company of New York, Inc., New York, N. Y.): The method of calculating the reactance of conductors in air and in iron pipe, as outlined in Del Mar's paper is a satisfactory empirical approach. The following analysis is submitted, therefore, not as a criticism of his method but as an attempt to put at least an approximate theoretical prop under it.

Reactance in Air

The only references to the reactance of large cables with which this writer is familiar are in papers by H. L. Curtis[1] and H. B. Dwight.[2] Curtis' solution, however, has the practical advantage that he has taken the trouble to evaluate his series and to compare them with tests over a wide range of spacings and frequencies. His solution, therefore, can be recalculated at 60 cycles.

In Figure 1 of the discussion, the lower group of curves show Curtis' data in the form of factors with which the reactance of a solid cylindrical conductor, determined by the standard formula, should be multiplied in order to obtain the true reactance of the conductor at 60 cycles. Curtis' treatment is for solid conductors, but it was assumed that his results would be applicable to stranded conductors as well. The reasonableness of this assumption is justified by the agreement obtained with test data as shown in Table I of this discussion.

The interstrand resistance factors k listed in Table II in my discussion of R. J. Wiseman's paper[3] were used to determine the R_{ac} for each cable size in the table. The slight discrepancy between tested and calculated values of the multiplying factor may be due to stranding, or possibly to differences between single-phase and 3-phase conditions. In any case, these differences are inconsequential and, in the opinion of the writer, the curves on Figure 1 of the discussion can be used with confidence for the evaluation of reactance of concentric stranded conductors in air.

Based on the test made on the 2,500,000-circular-mil segmental sector cable, the reactance of such conductors may be calculated on the basis of the standard formula for solid wire without any correction factor.

Reactance in Iron Pipe

Physically the effect of the iron conduit may be visualized as follows:

Judging from the tests made by ETL, and reported by Wiseman, the pipe materials have the magnetic properties of rolled sheet steel with an initial permeability well over 100. For all practical purposes, therefore, the reluctance of the iron conduit is negli-

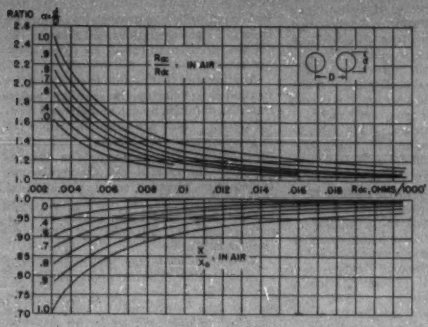


Figure 1. Chart based on Curtis' formulas

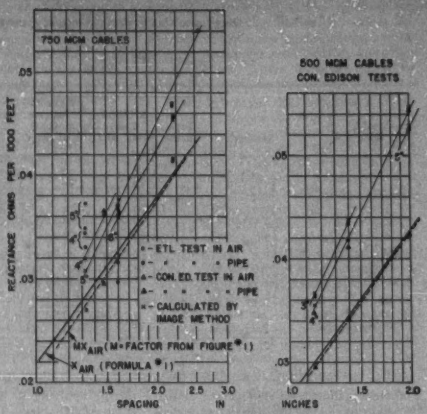


Figure 3. Three phase reactance of cables, triangular arrangement

gible compared with that of air and the conduit may, therefore, be considered as a surface of constant magnetic potential. The flux distribution in the air inside of the pipe is therefore the same as if the iron pipe were removed and in its stead, image conductors were placed at appropriate points.

For a 3-conductor system, there are three image currents spaced from the center of the pipe at a distance $b = R^2/e$ where R is the inside radius of the conduit and e is the distance of the actual conductor from the center of the conduit. This formula and the method in general are analogous to the one used in electrostatics except that the polarities of charge and image are opposite, whereas the polarities of current and its image are the same. See Static and Dynamic Electricity by W. R. Smythe.[4]

The method of images has been applied in electrostatics to the calculation of cable capacitance with limited success because the charges are not uniformly distributed over the conductor surface. Similarly, criticism may be levelled against the image method for the calculation of magnetic fields because of the nonuniform distribution of current and of the finite extent of the conductor area. The formula of image distance assumes that the current is concentrated in a filament. In order to improve the accuracy, therefore, the test results, or the corrected reactances obtained by the use of the charts in Figure 1 of the discussion may be used to determine an effective spacing of the current focus (fictitious point where the entire current may be centered).

Del Mar considers the effect of unequal current distribution as a shrinkage of the current carrying annulus from the inside and retains the actual spacing between conductors. I find that the method of images works much better if the effect of unequal distribution and consequent increase of resistance is considered as a shrinking of a solid conductor. This is not surprising because in reality for large cables at close spacing, the centers of current distribution are shifted towards one another, due to the crowding of the currents in the three conductors. On the basis of the tested or calculated a-c resistance and under the assumption of a solid conductor, a new geometric mean radius (d'_s) is obtained which is put in the standard reactance formula (Figure 1 of the paper) and a new effective spacing is calculated with the use of the reactance for air obtained either from test or from the charts. For example, in the case of 750,000-circular-mil cables spaced at 1.324 inches, the measured a-c resistance was 0.016 ohms at 20 degrees centigrade which is equivalent on d-c basis to a conductor cross section of 665,000 circular mils, hence $d'_s = 0.445\sqrt{CM} = 0.362$ inch.

With the location of the current centers thus determined, the position of the three image conductors is calculated. The formula for the reactance under the influence of all conductors including the images is derived in the Appendix. It is seen that the effect of the iron conduit is equivalent to a change in conductor spacing by a factor which is influenced by spacings of the three conductors with respect to one another and with respect to the pipe. This spacing correction factor contains distances between the conductors and the images and the writer found it quite convenient to take these

Table I

Conductor Size, Circular Mils	Spacing	d/D	Multiplying Factor for Reactance: Tested	Multiplying Factor for Reactance: From Chart
750,000	1.324	0.75	0.945	0.975
	1.598	0.62	0.97	0.985
	2.198	0.45	1.04*	0.99
1,500,000	1.7	0.83	0.93	0.93
2,500,000	2.17	0.84	0.885	0.885

* The test is obviously in error as the reactance measured is greater than the theoretical value for uniform current distribution.

Table II

Conductor	Tested Length, Feet	Reactance—1,000 Feet
350,000 cm stranded oval shaped cable. Over-all dimensions of conductor—0.78 inch × 0.47 inch; Dimensions over insulation 2.23 inches × 1.92 inches	43,000	0.062 ohms in 6-inch steel pipe
1,500,000 cm segmental sector cable. Diameter over skid wires × d = 2.9 inches *	41,700	0.0508 ohms in 8-inch steel pipe

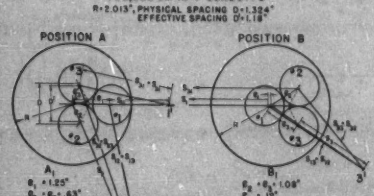


Figure 2. 750,000 circular mil cables in 4-inch conduits

R = 2.013 inches, physical spacing D = 1.324 inches, effective spacing D' = 1.18 inches

Table III. Spacing Correction Factors, Triangular Arrangement

$$X_{pipe} = 0.0529 \log_{10} \frac{KD'}{d_s}$$

Circular Mils	Spacing d/D	Pipe, Inches	K (From Test) 20°C	K (From Test) 80°C	K (From Image Method) 20°C	K (From Image Method) 80°C	Test By
500,000	1.2″ .. 0.67	3	1.35		1.39		Consolidated Edison Company of New York, Inc.
	1.2 .. 0.67	4	1.26		1.30		
	1.4″ .. 0.57	3	1.49		1.51		
	1.4 .. 0.57	4	1.35		1.39		
	2.0 .. 0.4	4	1.69		1.69		
750,000	1.5″ .. 0.66	4	1.35		1.36		Electrical Testing Laboratories
	1.3″ .. 0.75	4	1.3	1.38	1.24	1.24	
	1.3 .. 0.75	5	1.41	1.56	1.24	1.24	
	1.6″ .. 0.62	5	1.39	1.31	1.2	1.25	
	1.6 .. 0.62	6	1.37	1.31	1.18	1.26	
	2.2″ .. 0.46	6	1.26				
1,500,000	1.7″ .. 0.83	5	1.17	1.2	1.17	1.21	
2,500,000, concentric	2.2″ .. 0.84	6	1.12	1.15	1.13	1.15	
2,500,000, segmental	2.1″ .. 0.84	6	1.19	1.22	1.21	1.24	

distances off scale-correct drawings. The procedure is illustrated in Figure 2 of the discussion for one of the conductors tested by ETL. On the left-hand side is the disposition of the conductors and the images for one position of the twisted cables in the conduit. On the right-hand side are shown the conditions when the three conductors have been twisted 180 degrees with respect to their position on the left-hand side. Due to the symmetry of the arrangement, some of the distances in the general correction formula are equal, with a corresponding simplification of calculations. Good accuracy is obtained by taking the average of the two correction factors obtained for the positions shown.

For untwisted cables the position on the right-hand side of Figure 2 of the discussion is more probable, and the calculations were made under the assumption that two of the cables are in contact with the pipe. The spacing correction factors for this arrangement are slightly greater.

Figure 3 of the discussion is a composite picture applying to 750,000- and 500,000-circular-mil cables. The solid heavy line is the reactance in air determined by the standard formula, while the dotted line is the corrected reactance due to unequal current distribution as obtained with the help of the factors given on Figure 1. The test points in air are also shown. The agreement is good except in the case of one of the test points at 1.6 inch spacing which Del Mar himself eliminated and two test points at 2.2 inches which lie above the solid line and must obviously be in error. The tests on cables in conduit are shown by the solid symbols and the calculated reactances using the image method are given by crosses. Good agreement between tests and calculations is evident except in the case of the 1.3-inch spacing, but the writer is inclined to accept the calculated point as more representative because of the too large variation of reactance with temperature, and also because the reactance in the larger conduit was found to be larger which is contrary to theory. The calculations and tests on the 500,000-circular-mil cables are in excellent agreement.

Another way to illustrate the value of the method is to compare the space correction factor as obtained by calculations and from the tests. These are given in Table III of the discussion for all tests on triangular configuration. The reason for the discrepancies in the case of 750,000-circular-mil cables was already noted. Additional evidence regarding the usefulness of the method is given by the good agreement in the case of 1,500,000- and 2,500,000-circular-mil cables.

No claim is made that the image method as simplified for practical application is exact, yet it appears to be a reasonable, simple and straight-forward way to calculate the reactance in iron conduits for all practical cases of triangular configuration.

In conclusion, the field test data given in Table II of this discussion are added to the treasure house of information on the reactance of cable in random dispositions. The field tests on 1,500,000-circular-mil cable, compared with item 10 of Table IX in Del Mar's paper, indicates a 20 per cent increase for random disposition.

Table IV

Test Number	Conductor Temp C	Impedance Ohms	Phase Angle	A-c Resistance Ohms/1000 Ft	Reactance Ohms/1000 Ft	D-c Resistance Ohms/1000 Ft	A-c/D-c Resist. Ratio
1	11	2.149	68.71	0.0222	0.0570	0.0168	1.32
2	36	2.189	67.41	0.0239	0.0575	0.0185	1.29
3	54.5	2.197	66.28	0.0250	0.0572	0.0197	1.27
(Est.)	70	2.236	65.58	0.0262	0.0579	0.0208	1.26

Table V

	Brieger		Sinclair
Circular mils	1,500,000 (4 segment)	350,000	600,000
Measured reactance			
Ohms per 1,000 feet	0.0508	0.062	0.0579
Calculated reactance			
Pipe inside diameter, inches	8	6	6 1/8
A-c/d-c resistance ratio in air by			
Wiseman formulas for R''/R	1.099	1.012	1.035
Assumed spacing, inches	2.90	2.075	2.315
Ohms/1,000 feet triangular	0.0427	0.0553	0.0494
flat	0.0512	0.0664	0.0592
random	0.0491	0.0635	0.0568
Ratio of measured to calculated triangular	1.19	1.12	1.17
Ratio of measured to calculated random	1.03	0.98	1.02

Appendix

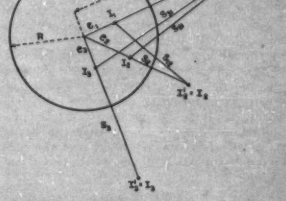
3-phase currents—$I_1, I_1 e^{-j120}, e^{-j240}$

GMR of conductors $= a$

Distance between conductors $= d$

Distances between conductors and images: S_1, S_2, S_3; S_{12}, S_{13}; S_{21}, S_{23}; S_{31}, S_{32}

Linkages around conductor number 1 (I_1)



$$L = 2I\left\{\ln\frac{1}{a} + \ln\frac{1}{S_1} + e^{-j120}\left(\ln\frac{1}{d} + \ln\frac{1}{S_{12}}\right) + e^{-j240}\left(\ln\frac{1}{d} + \ln\frac{1}{S_{13}}\right)\right\}$$

as

$$e^{-j120} = \frac{1}{2} - j\sqrt{3/2}$$

$$e^{-j240} = \frac{1}{2} + j\sqrt{3/2}$$

$$\therefore L = 2I\left\{\ln\frac{d}{a}\frac{\sqrt{S_{12}S_{13}}}{S_1} + \frac{\sqrt{3}}{2}j\ \ln\frac{S_{12}}{S_{13}}\right\}$$

The imaginary term is a fictitious resistance, the real term is the inductance. Average linkages of all three phases

$$L_{aa}=2l\left\{\ln\frac{d^3}{a}\sqrt{\frac{S_{12}S_{13}S_{21}S_{23}S_{31}S_{32}}{(S_1S_2S_3)^2}}+j\ldots\right\}$$

thence the inductance

$$L=2\ln\frac{d}{a}\sqrt[6]{\ldots\ldots}=2\ln K\frac{d}{a}$$

$$K=\sqrt[6]{\frac{S_{12}S_{13}S_{21}S_{23}S_{31}S_{32}}{(S_1S_2S_3)^2}}=\text{spacing correction factor}$$

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1. SCIENTIFIC PAPER NUMBER 374, National Bureau of Standards (Washington, D. C.), 1920.

2. PROXIMITY EFFECT IN WIRES AND THIN TUBES, H. B. Dwight. AIEE TRANSACTIONS, volume 42, 1923, pages 850–9.

3. See reference 1 of the paper.

4. STATIC AND DYNAMIC ELECTRICITY, W. R. Smythe. McGraw-Hill Publishing Company, New York, N. Y., pages 282–86.

W. A. Sinclair (The Detroit Edison Company, Detroit, Mich.): Del Mar, Salter, Wiseman, and the Insulated Power Cable Engineers Association have made a very valuable contribution to the data required for determining ratings of pipe-type cables.

Results of a series of tests conducted on the 35,130-foot Delray-Warren line of the Detroit Edison system provide an excellent opportunity to determine the reactance of 600,000-circular-mil conductors in a steel pipe, with random arrangement. This 120-kv high-pressure gas-filled cable line consists of three SMD-type single-conductor cables installed in a $6^1/_2$-inch inside diameter by 1/4 inch wall, mild steel pipe. The concentric-round conductors are insulated with 600-mils preimpregnated paper over which is a bronze tape intercalated with saturated cotton tape, cotton webbing tape lapped and two half-round copper skid wires 200 by 100 mils applied open spiral, double start. The diameter over the skid wires is approximately 2.315 inches which means that for this line 38 per cent of the pipe is occupied.

Before circulating any current in the conductors, the d-c resistance of the total cable was measured by means of a portable Kelvin double bridge. This value, along with measured temperatures, was used as a reference value for all conductor temperature computations.

Test current for the positive sequence impedance measurements was supplied by six 500-kva transformers connected to give an open-circuit phase-to-ground voltage of about 1,290 volts. Table IV of the discussion gives the impedance values obtained with 530 amperes along with the respective calculated values of resistance and reactance, and the data show the influence of cable temperature on the reactance to be negligible, and at 70 degrees centigrade the 60-cycle reactance was found to be 2.2 times the a-c resistance.

William A. Del Mar: Brieger's theoretical treatment is a valuable contribution to the subject serving to check the consistency of the experimental data and both to interpolate and extrapolate for conductor sizes and spacings not included in the tests. The conception of the steel pipe as a surface of "constant magnetic potential" and therefore as a mirror of the cables in the pipe, is particularly valuable.

The test data given both by Brieger and Sinclair give values of reactance of cables installed in pipe which check quite well, that is within 3 per cent with those calculated by the formulas in my paper for random arrangement, assuming axial spacings, for the triangular arrangements, equal to the overall diameters. These data are given in Table V of the discussion.

Wiseman's paper does not give a procedure for calculating the skin plus proximity effect factor for segmental cable. The skin plus proximity effect ratio, R''/R was worked out for 1,500,000 circular mils for concentric stranding in air using equations 1 and 2 from the Appendix (Wiseman's paper). The skin effect ratio, R'/R or $f(x)$ for 1,500,000-circular-mil cable from Table XV of Wiseman's paper was subtracted from the former ratio, giving the proximity effect alone. The skin effect ratio for segmental 1,500,000-circular-mil cable was then derived in the usual way by taking that of a concentric cable of 44 per cent of its cross-section. This was added to the proximity effect, as derived above, to obtain the skin plus proximity effect for the segmental cable, given in Table V of the discussion as 1.099.

This value is somewhat lower than shown by actual tests of 1,500,000-circular-mil segmental cable, due, probably, to the empirical method used. When an experimental value is used in the described calculations, the ratio of measured to calculated triangular arrangement is 1.20 as indicated by Brieger, and the ratio of measured to calculated random arrangement is 1.05.

It is of interest to note that while the ETL tests were made on cable without metallic covering, the Brieger and Sinclair tests were made on finished cables with metallic tapes and skid wires. These appear to have no appreciable effect on the reactance.

Polyethylene Insulated Coaxial Cable for Carrier Current Control Circuit

H. B. SLADE
ASSOCIATE AIEE

F. P. WEST
ASSOCIATE AIEE

A POWER circuit consisting of 138-kv single conductor 1,250,000 circular-mil oil-filled cables was recently installed between the Sherman Creek generating station of the Consolidated Edison Company of New York, Inc., and the Dunwoodie substation of the Yonkers Electric Light and Power Company, a distance of approximately 8.1 miles. This underground power circuit connects to a 138-kv overhead circuit at the Dunwoodie substation which continues into upper New York state to Millwood, a distance of approximately 21 miles.

Previous to the installation of the 138-kv underground circuit from Sherman Creek to Dunwoodie, the overhead line from Dunwoodie to Millwood had been operated with carrier current superimposed on the high-tension line. It was necessary to extend the carrier circuit to Sherman Creek, since the underground cable was teed to the overhead line at Dunwoodie without circuit breakers. The underground power cable, which was installed with insulating joints and cross-bonding, could not be used for the carrier current circuit because carrier frequency tests conducted several years ago on a similar underground feeder showed that the decibel losses were too high for this use. Thus, it became necessary to install a separate carrier circuit cable.

The cable for the carrier current circuit is installed in the same underground system with the power cable and terminates at the same stations. A cable of the coaxial type was selected consisting of a number 13 stranded copper conductor, 185 mils of polyethylene insulation, a woven copper braid shield and a 78 mils thickness neoprene outer jacket. The cable connects to transmitting and receiving equipment at Sherman Creek and Dunwoodie stations and the circuit also is tied in with similar equipment at the Millwood substation by way of the overhead power line. The carrier current frequency is approximately 60 kc.

The carrier current circuit is used in conjunction with high speed directional relays which protect the power circuits. Carrier current signals are transmitted to block tripping of the high speed relays when a fault occurs outside of the protected circuit.

Coaxial Cable

With the problem of transmitting carrier frequencies over a number of miles with low loss, the choice of dielectric would naturally be between air (with spacers between conductors such as the Bell Telephone System uses) or solid dielectric. Since the circuit is approximately 8.1 miles in length, it was decided to use solid dielectric coaxial cable as being simpler to install and maintain. Of all the solid dielectrics, polyethylene has the most promise for low-power high-frequency transmission, possessing as it does a dielectric constant of 2.3, a power factor of 0.0005 and having unusually stable characteristics over normal ambient temperature ranges and in the presence of water.

The carrier transmitting and receiving equipment required a cable with approximately 70 ohms characteristic impedance and with an attenuation within specified maximum limits at frequencies from 50 to 150 kc, so that the total losses would be kept within the capacity of the equipment.

Since there existed a more or less standard Army-Navy type which met these requirements, this design was selected. The outer shielding braid or conductor possessed so much lower resistance than the center conductor that for practical purposes, the only factors influencing attenuation at the specified frequencies were the resistance of the center conductor, the characteristic impedance, and the proper termination of the cable.

The outer sheath of neoprene was chosen as a noncontaminating material of known long life because of its resistance to oil, flame, weathering, water, and heat.

Manufacture of the cable required precise extrusion and annealing of the polyethylene insulation. The annealing was accomplished by running the freshly extruded insulated wire through 60 feet of troughs filled with water with a starting temperature of 200 degrees Fahrenheit and ending at 110 degrees Fahrenheit. The resulting insulation was formed tightly around the 7-strand conductor to minimize the possibility of voids. The insulated and shielded conductor was finally sheathed with neoprene which was vulcanized at a low temperature so as not to injure the polyethylene dielectric which softens and flows at 230 degrees Fahrenheit.

Tests on the reels of cable included dielectric strength, 15-kv alternating current for five minutes, to guard against imperfections in the insulation, copper resistance tests to guard against open circuits and as a check on wheel measured footage, and attenuation tests performed by a one-quarter wave length resonant frequency measurement developed by the manufacturer for testing carrier frequency cables.

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H. B. SLADE is a research engineer with The Okonite Company, Passaic, N. J. and F. P. WEST is an assistant engineer with the Consolidated Edison Company of New York, Inc., New York, N. Y.

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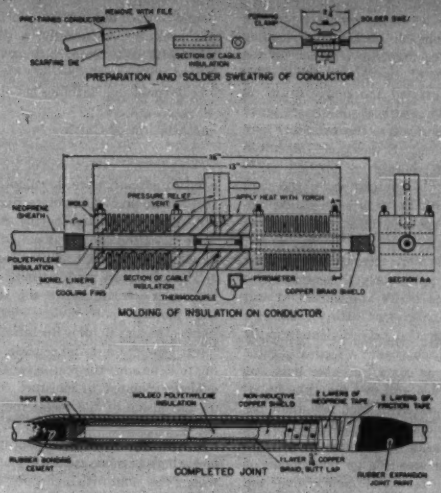

Figure 1. Joint for polyethylene insulated coaxial cable

Splicing

In planning the joints for the coaxial cable, greater attention was given to the formation of a solid section of polyethylene bonded to the insulation of each cable end, offering no chance for moisture to enter and cause failure or change in characteristics of the cable. Splicing tapes of polyethylene with an adhesive layer were found inferior due to water seepage. A molded joint of wrapped plain polyethylene tapes was chosen instead as having the longest life. For simplicity of molding, a cylindrical mold was chosen, with the same diameter cavity as the cable insulation diameter. This was consistent with factory repair technique in that the repair is the same size as the cable, and capable of withstanding factory test voltage and all other tests. At first, it was considered best to form a silver soldered butt splice of the conductor, but for field work, a split sleeve connector filled with solder was proposed. Most of the splicing development was done using these connectors, but eventually the chance for electrical reflections from frequent and irregularly spaced splices of different surge impedance than the cable proper, indicated a return to a splice of the same dimensions as the conductor. The Consolidated Edison Company developed a split aluminum forming clamp (Figure 1) to hold the conductor ends in position while soldering to effect a strong conductor splice of the same diameter as the conductor. The conductor ends were previously scarfed in a die (Figure 1).

In designing the mold itself, numerous factors were considered, tried, kept or discarded. One of the first facts recognized was that polyethylene has a high coefficient of expansion between room temperature and its flow point, and an even higher coefficient above its flow point. The second fact is that polyethylene remains quite firm up to its flow point and then changes to a very soft plastic above its flow point. Under conditions of pressure, the range of temperature from solid to plastic is very small.

Some rubber compositions of a similar high expansion nature had been molded successfully only after extreme care was taken to clamp the cable further back on firm relatively cool insulation, otherwise the cable would push itself out of the mold. Also, where very soft plastic is present in the center of a mold, it is good practice to hold the cable rigid to prevent eccentricity. After a number of molds were made and discarded in attempts to hold cable in place while trying to make a molded polyethylene joint, a mold almost identical to that shown in Figure 1 was made and splices were attempted using 100-watt 115-volt electric heating elements (controlled by a Variac) imbedded in the center section of each half of the mold. Measurements of temperature showed that at about 60 volts on the electric heating elements, the temperature would be 240 to 250 degrees Fahrenheit in the center section, tapering along the finned section to 150 degrees Fahrenheit at the ends. The small hole in the center section was considered to be about the hottest spot and in it was placed a thermocouple, indicating the temperature on a pyrometer. Although the temperature of 250 degrees Fahrenheit for 15 minutes usually sealed tapes and insulation, any compound flashing from the mold was found to indicate a gas pocket next to the conductor. The gas pockets occurred usually near the ends of the center heating section and were thought to be due to numerous causes, including residual soldering flux, leakage of the mold, and hot spots. Extra care in splicing, and tests on plain insulated unspliced cable eliminated the question of excessive flux, and careful checking of temperatures by surface pyrometers at first failed to show hot spots. The joints made similar to Figure 1 frequently had gas pockets under the original insulation. In an attempt to correct the leakage from the mold as a possible cause, a longitudinal liner of 5-mil tinned copper tape was shaped more than half-way around the cable to cover the joints of the mold. The first successful joints were made this way with no bubbles or gas pockets and with only a slight indication of pushing out at the ends of the mold. The correction of leakage seemed to be the answer, and the rest of the splicing procedure was worked out using copper braid ribbon to continue the shielding, with neoprene and friction tapes over all to complete the sheath covering.

Before ordering molds for the field work, it was decided that a 115-volt a-c power supply was not convenient enough along the cable splicing route to make it a practicable way for heating. Consequently, attempts were made to heat the mold with a torch. Such heating was found to be quite suitable and was proposed in the splicing instructions. Also, the 5-mil tinned copper tape mold liner was found to be too soft for general use and a 5-mil monel tape was found more suitable.

As a means of reducing the high-frequency impedance of the shielding on the finished joint, 5-mil tinned copper tape was laid on the insulation under the wrapped copper braid, since the high-frequency currents would otherwise follow the convolutions of the braid

and result in slightly higher losses at the splice.

Since the joint design was new and involved splicing techniques which were new to the splicers, it was considered desirable to have the splicers first make some joints in the Consolidated Edison splicing school in order to train them, and to remove any possible jointing difficulties before installation in the field.

While molding some of the joints in the school, it was found that blow holes occurred in the insulation within the center portion of the mold and more frequently near the points where the hand-applied polyethylene tape joined the factory-extruded insulation. This was first noticed while experimenting with thin copper strips as gaskets between the mold halves to prevent flashing instead of the cylindrical shaped monel liner used previously. The monel liner interfered with assembling the mold over the insulated conductor and sometimes required considerable time even under ideal working conditions in the school. After molding several joints, with and without the use of the monel liner, it was found that the blow holes were forming in the insulation only when the liner was not used. From this experience, it was concluded that the cylindrically-shaped monel liners not only prevented flashing but apparently smoothed out the heat transfer from the outside to the inside of the mold, particularly at the end points of travel of the torch flame as it was played back and forth on the mold. It was decided that the liner was a necessary part of the design, but in order to facilitate the assembling of the upper half of the mold the liner was cut lengthwise in two halves. This permitted more flexibility in adjusting the position of the liners in order to prevent them from catching against the edge of the mold during assembly. The liners were shaped around a mandrel and were used several times before they needed to be replaced. Since they were only 5-mils thick, the outer surface of the molded polyethylene insulation was not made irregular enough to require smoothing before applying the shield.

The original joint design called for the application of polyethylene tape over the conductor, built up to approximately the diameter of the cable insulation. It was observed while molding the joints that the volume of applied polyethylene in a melted state was important and should be controlled within minimum and maximum limits. If the volume was too small, voids were formed. If the volume was too large, excessive pressure which developed during the heating process forced the cable ends too far out of the ends of the mold. This cable movement exerted too much strain on the conductor which in some cases broke the connection where the two ends were joined together. It was found difficult to control the volume of applied polyethylene by the application of tape since it varied with the way each splicer applied the tape and measured the completed jacket.

In order to more accurately control the amount of applied polyethylene and also to save time in its application, it was decided to experiment with the use of a length of extruded polyethylene from the cable for this purpose. A section of cable insulation was cut from cable that was to be discarded and to a length approximately one-sixteenth inch less than the exposed conductor. A slit the width of two hacksaw blades was cut longitudinally on one side of the section of insulation to permit slipping it over the conductor after the two ends had been solder sweated together. The use of the preformed insulation also simplified the joint further by eliminating the need for penciling the cable insulation. When exposing the conductor for splicing, the insulation was cut circumferentially with a dull hot knife to prevent cutting any of the conductor strands.

Even with a closer control of the amount of applied insulation, the cable moved longitudinally out of the ends of the mold during the heating process. The pressure which developed during the heating of the mold was beneficial in reducing the shrinkage during the cooling process, but in some cases the cable ends moved apart enough to weaken or partly break the soldered joint in the conductor.

To relieve some of the pressure which developed within the mold during the heating process and to reduce the corresponding longitudinal movement of the cable from the mold, a vent was made at the center of the mold. The size of the vent hole was made small enough to permit sufficient pressure to develop to mold the polyethylene properly and at the same time keep the cable movement within safe limits.

The explanation of the necessity for using monel (or tinned copper) tape between the mold and insulation appears to be the better distribution of heat along the center section of the mold. The use of the vent proved that leakage of compound of itself did not cause formation of gas pockets. Apparently, a plain iron mold with bolts, guide pins and fins and heated with electric heating elements or an external flame develops hot spots.

Installation of Cable

Two coaxial cables, one of which is for a future circuit, were installed in one of the 5-inch precast concrete ducts without apparent damage to the cable. In some cases the cable was pulled through manholes and racked without joints.

The joints were made without difficulty but voids were found in the surface of the molded polyethylene in several joints after the molding operation. These joints were cut out and remade. In trying to determine the reason for the voids, evidence pointed to moisture condensed on the inside of the metal mold before its use as a possible cause. The trouble was associated more with damp manholes and where extreme care was not used in wiping the mold with a cleaning fluid just previous to its use.

Tests on Circuit

During the installation of the cable, approximately 2.6 miles of circuit was tested by the Consolidated Edison Company by the voltage drop method. This length, including 24 joints, showed an attenuation of 0.28 decibels per 1,000 feet at 60 kc.

Later during the installation, 3.5 miles of circuit was tested by Okonite engineers for attenuation using a one-quarter wave length resonance method developed during the war for factory testing of reel lengths. The two cables tested in this manner yielded about 0.28 decibels per 1,000 feet at 60 kc, including 28 joints.

Upon the completion of the installation of the cable, the Consolidated Edison Company tested the completed length at 60 kc and determined that the losses were still 0.28 decibels per 1,000 feet. The circuit contained 61 joints.

In comparison with tests made on the cable in the factory, the splicing of the cable did not increase the losses of the circuit.

Conclusion

From the field installation and results of tests, it is felt that this type of cable and joint is practical for installation in a conduit and manhole system designed for power cable.

Discussion

L. F. Hickernell (Anaconda Wire and Cable Company, Hastings-on-Hudson, New York): The authors state that "the outer sheath of neoprene was chosen as a noncontaminatory material of known long life because of its resistance to oil, flame, weathering, water, and heat." Also, that "the insulated and shielded conductor was finally sheathed with neoprene which was vulcanized at a low temperature so as not to injure the polyethylene dielectric which softens and flows at 230 degrees Fahrenheit."

In view of the difficulty of obtaining adequate vulcanization of neoprene at low temperatures, the question arises as to why neoprene was selected in preference to polyvinylchloride (a plastic which requires no vulcanization). As is well known, this material is used extensively by the Army and Navy for noncontaminating jackets for similar types of cable.

M. W. Ghen (Duquesne Light Company, Pittsburgh, Pa.): This paper by Slade and West is of timely interest. While it deals only with the splicing of a single type of small control cable, it indicates the difficulties encountered and impresses the engineer with the fact that what appeared to be a simple operation turned out to be quite complicated. Whether or not such difficulties are to be multiplied in proportion when the method is tried on larger cables remains to be seen.

With the increased use of plastics, rubber, and rubber-like compounds, there is room for research into splicing techniques. Any method which results in a splice as nearly like the cable itself as possible is worthwhile. It would appear that the authors have at least pointed the way to the development of satisfactory field splicing of the types of cable mentioned.

The reference to the training of cable splicers and the subsequent difficulties encountered, proves the wisdom of thoroughly instructing field forces in new methods even though study engineers may believe that the procedure has been reduced to a relatively simple process.

The authors are to be complimented upon their thoroughness in solving this problem.

William A. Del Mar (Phelps, Dodge Copper Products Corp., Yonkers, N. Y.): The design of joint described by the authors illustrates both the advantages and the difficulties encountered in the use of polyethylene insulation. A joint integral in material and structure with the cable insulation, such as theirs, is certainly desirable, but it is questionable whether it is practicable for cables of a size much larger than that described in the paper.

Not only are the expansion and contraction difficulties accentuated as far as bubbles are concerned but, as the joint cannot be annealed, as is necessary in making the cable, it will carry internal stresses which are likely to cause trouble under operating conditions.

The use of neoprene compound instead of a noncontaminating vinyl plastic seems like experimenting where a vast amount of operating experience is available. What assurance have the authors that the neoprene compound is noncontaminating? The Geon type of vinyl plastic, as used on Army and Navy high-frequency cables, would seem to be the logical sheath material.

H. B. Slade: The choice of the outer sheath for the carrier current coaxial cable was made primarily on its noncontaminating nature and its long-time operating experience. The difficulties of vulcanizing the neoprene at a low temperature were not a great problem and data in the manufacturer's laboratory indicated that a noncontaminating polyvinyl chloride-type sheath was more permeable to water than a low temperature vulcanized neoprene sheath.

It is well recognized by the authors that a joint made in larger cables might not be as suitable as the type developed, but it was considered that the cable in question operating as it does at a low voltage would not suffer from any slight irregularity in the completed joint. The insulating wall on the cable is heavier, not for purposes of dielectric strength, but to obtain the proper surge impedance for the carrier current circuit.

Insulator Surface Contamination

H. A. FREY
MEMBER AIEE

ONE OF THE major unsolved problems in the insulation of electric power lines is that of obtaining trouble-free operation under conditions of severe atmospheric contamination. This is a problem which has faced the industry for many years. Recently, because of expanded industrial activity, the difficulty seems to be growing in magnitude. Despite the importance of the problem, there is no known general solution. In some particular areas, successful operation has been obtained by periodic washing, by over-insulation, or by the use of special insulators. None of these things, however, can be regarded a general satisfactory solution which can be universally applied.

It is the intent of this paper to outline the problem and to show how the characteristics of insulators are affected under extremely dirty conditions which are probably approaching the worst limit of conditions encountered in service.

The Effects of Surface Dirt on Insulators

The presence of conducting films on the surface of outdoor insulators subjected to high voltages leads to a number of very serious operating conditions. These may be briefly summarized as follows:

1. *Leakage Currents.* If the surface of the insulator were completely coated with a conducting film, leakage currents would flow between the line and ground, and the magnitude of these currents would be inversely proportional to the total resistance of the film. In practice, the surface need not be completely coated with the conducting material in order to permit the passage of leakage currents of serious magnitude. If some narrow bands remain nonconducting, local sparks form through the adjacent air gap, thus shunting the leakage currents past the high-resistance zone. If leakage current is severe, it may result in the charring of wood insulator pins or wood cross arms and the hazard of pole top fires.[1]

2. *Flashover.* The presence of a conducting surface film on an insulator invariably results in some distortion of the voltage gradient over the insulator. Even if the surface film were of uniform resistivity, the resistance of the leakage path would vary due to the hoods, corrugations, and other contours of the surface. Actually the surface is seldom uniformly dirty or uniformly moist so that large variations in the surface resistance may be expected between different zones on the surface of any one dirty insulator. Those zones having the highest resistance are of course subjected to the highest voltage gradients. If the voltage across any one zone becomes high enough, the air gap in parallel with that zone will breakdown forming a local spark. Figure 1 shows such local sparks forming on a dirty insulator under test. These local sparks further distort the gradient over the surface, and the stress concentration at their extremities tends to promote growth of the spark. If several such sparks occur simultaneously on the same insulator, there is a hazard of flashover and loss of service.

3. *Puncture.* The local sparking which occurs over the surface of dirty insulators results in considerable surface heating. If the combination of local voltage gradients and local temperature rise is sufficient, there is danger of puncture of the insulator. Under such conditions, puncture may be caused either by thermal cracks set up by intense local heating and differential expansion, or by the increase in the temperature of the dielectric material itself above its safe operating point. As the temperature rises, the resistance of most insulating material rapidly diminishes. Hence leakage currents begin to flow through the material itself. This results in more energy dissipation and more heating until ultimate breakdown occurs.[2] It is quite probable that insulator failures from this cause in dirty areas are more frequent than is generally realized.

4. *Corrosion.* As a result of operation in dirty areas, greatly accelerated corrosion of metallic parts immediately adjacent to the insulators has been observed. This corrosion may be occasioned by any of several actions. In the first place, under some conditions, the leakage currents which flow through the conducting film may exhibit directional properties so that some electrolytic effects may be observed at the terminal electrodes.

Also, in cases where violent corona or local arcing occur, the formation of ozone and nitric acid may accelerate corrosion immediately adjacent to the insulator. This is particularly possible in cases where the metallic elements affected (for example, insulator pins) are shielded from air currents which would remove the products of decomposition of the air.

A third cause of localized corrosion may be heating. Again in places where the metallic elements are shielded from air currents, local heating, due to leakage currents and sparking may considerably accelerate normal corrosion rates.

Perhaps one of the most common causes

Figure 1. Local sparking on a dirty wet insulator

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H. A. Frey is chief development engineer at Locke Incorporated, Baltimore, Md.

Figure 2. Corrosion around the pin of a suspension insulator after 17 years service in a contaminated atmosphere

of localized corrosion is the burning effect of the arcs themselves. When localized sparking occurs over a long period of time and terminates on one of the metallic electrodes, the galvanizing at the surface is damaged or destroyed, exposing the base metal to the normal corrosive action of the atmosphere. Some cases have been observed on suspension insulators where corrosion of the insulator pins at the cement line has been so severe as to cause bursting of the insulators. Figure 2 shows the products of corrosion around the pin of a standard suspension insulator which had operated in a contaminated atmosphere for about 17 years.

We may then summarize the effects of dirt on insulators as follows: leakage currents, with consequent hazard of wood burning; power flashover; insulator puncture; and corrosion of adjacent metallic parts.

Types and Quantities of Dirt Which Cause Trouble

There is probably no operating area where outdoor insulators are not subjected to some surface dirt to a greater or lesser degree. This is well recognized, and all insulator designs are prepared with this in mind. Difficulties are not encountered until the dirt concentrations on the surface become very severe, or until abnormally conducting dirts are encountered. In general, any conducting material deposited on the surface of an insulator will affect the insulator performance to some extent. The dirts commonly experienced in the United States have little effect on the performance of insulators so long as the surfaces are dry. Salt, soot, cement, and most of the industrial dirts cause no serious problem so long as they are dry. As soon as such materials are moistened, however, some phases of the dirt go into solution to produce a conducting film. In the presence of large quantities of water, the conducting elements are often quickly leached out, and the dilution of the solution is so great as to produce relatively low conductivity. Hence serious troubles are not often experienced during heavy rain storms. Likewise in areas where heavy rains are frequent, the insulator surfaces are usually automatically washed, and the conducting phases leached away so that trouble is relatively rare. The worst conditions are usually experienced in the presence of fogs, mists, and light drizzly rains. Under such conditions, moisture is provided to produce conducting films on the insulator surfaces, and yet the quantity of moisture present is not sufficient to seriously dilute the solution, or to wash off or leach away the conducting elements. These conditions are usually at their worst when the rain or fog occurs after a relatively long dry spell, during which period, quantities of dirt have had an opportunity to accumulate on the surface.

From this we would expect that the worst kinds of dirt which could be experienced would be those containing a high percentage of soluble matter. This is quite well borne out in practice. Thus, ordinary sodium chloride is extremely severe. Common industrial dirts which contain high percentages of soluble salts, such as fly ash, railroad soot, cement dust, phosphates, and so forth, are very severe, while they remain on the surface. Some of these materials which are almost 100 per cent soluble, such as sodium chloride, will quickly wash off unless they are bound by other inert materials which cake on the surface. Practically all dirts which accumulate on insulator surfaces are a mixture of a number of things. They may consist of fly ash, soot, salt, or other active materials, mixed with relatively large quantities of ordinary wind-blown soil and dust which bake on the surface and influence the washing action of the soluble components.

In addition to retarding the rate of washing, the inert materials also tend to increase the thickness of the water film on the surface. In other words, because of their capillary action they act like blotting paper in maintaining a thick water film on the surface. By increasing the thickness of the water film, they reduce the resistivity of the leakage path and render the operating condition more serious.

Those dirts which have great cementacious properties are, of course, most serious. Portland cement dust, for example, will tend to build up on the surface, layer after layer, to a thick crust, which is firmly bonded, and which cannot be washed away. Other materials, such as some of the phosphates, exhibit this same property. Very often, industrial or railroad smoke carries with it bituminous compounds which firmly bond the other phases of the dirt to the surface of the insulator.

Also many of the commonly experienced dirts are quite hygroscopic so that they are seldom entirely dry. Ordinary sodium chloride is of course very hygroscopic, and Portland cement exhibits this property to a considerable degree. The dirt accumulation on insulators is seldom static for any considerable period of time. It is constantly being built up by the deposit of additional material and washed or leached away by the action of wind and rain. The quantity of dirt present on the surface of any particular insulator therefore will be controlled by:

1. The rate of deposit of dirt.
2. The frequency and intensity of washing rain and wind.
3. The nature of the dirt, that is, its ability to cake and bond to the insulator surface.

The Resistance of Dirt Films

As pointed out previously, most of the commonly experienced dirts are seriously conducting only in the presence of moisture. The conductivity is due to the solubility of the dirt and the formation of low-resistance electrolytes. The resistivity of most electrolytes is inversely proportional to the number of parts per million of soluble matter in solution. For the same solution concentration, the resistivity of most electrolytes will be influenced also to some extent by the molecular weight of the salt which is in solution. Salts of high molecular weight will generally produce electrolytes having high resistivity and vice versa. In practice, the variations in resistivity due to solution concentration greatly outweigh those due to the nature of the particular salt which is in solution.

The resistance of the surface film on any circumferential element of a dirty insulator will be directly proportional to the product of the resistivity of the dirt and the length of the element, and will be inversely proportional to the cross-sectional area of the dirt film at that element. The cross section of the dirt film is in turn the product of the thickness of the film and the circumference of the element. The resistance from point to point in the leakage path over the surface of a dirty

insulator will therefore vary with the resistivity of the dirt, the thickness of the dirt film, and the circumference of the insulator at those points. It is this variation in surface resistance from point to point which results in the distortion of voltage gradient.

The resistivity of the dirt film on insulators in service varies over a wide range. In some areas, the coatings must build up to considerable thickness before trouble results. Other types of dirt may cause serious trouble when the thickness of the film is so small as to be almost invisible. A typical example of the latter is nitrated cotton lint.

Some idea of the relative severity of different types of dirt can be gained from a simple test. A weighed sample of the dirt is mixed with a measured quantity of distilled water, the mixture is filtered, and the resistivity of the filtrate measured. This produces an arbitrary measure of the relative severity of that particular dirt in the presence of moisture. In Table I are listed the results of measurements of this type made on several commonly experienced dirts. One of the questions which is always involved is the speed with which the conducting agents are leached out of a particular dirt. This provides some information on the speed of cleaning during normal rains. Some measure of this can be obtained by taking the residue of dirt which has been filtered out of the first mixture, remixing in the same proportions with distilled water, again filtering, and measuring the resistivity of the filtrate.

The change in resistivity of the filtrate from the second mixing gives some idea of the speed of leaching for the particular dirt. Table I includes the results obtained by remixing the residue from the first test. It was, of course, impossible to remix the residue of cement since it had set after the first trial. In these tests flint was included as the control since it is generally regarded as a relatively insoluble material. However, it should be noted that even commercial flint contains sufficient soluble matter to appreciably effect the resistance of distilled water which was used in the mixture. The initial resistivity of the distilled water before mixing was more than 150,000 ohms per centimeter cube. After the first mixing the resistivity of the solution which has been in contact with the flint was reduced to 21,000-to-23,000 ohms per centimeter cube. It should be noted that the fly ash used in these experiments had been taken from the collectors of a power plant where it had been hosed down to control dust. Hence, it is quite probable that some of the soluble matter had been leached out before the test sample was obtained.

This method of investigation may have practical value in estimating the severity of the dirt encountered in particular locations.

Laboratory Flashover Tests

For many years laboratory tests have been made by operating companies and by insulator manufacturers to study the performance of insulators under dirty conditions, and to compare the relative performance of various insulator designs. All laboratory tests of this nature are limited in their simulation of actual operating conditions. The results of the tests will be influenced by the quantity and nature of the dirt used, by the degree to which the surface is moistened, and by the distribution of the dirt and moisture pattern on the surface of the insulators. In service, where insulators are continuously energized, the actual pattern of distribution of the dirt on the surface may be controlled by the voltage gradient around the units, as well as by the washing and eroding action of rain and wind. It is very difficult to be sure that a laboratory test truly represents the operating condition which would exist after months of exposure in a particular location. The results of such tests must therefore be used with caution.

Despite their limitations, laboratory tests can furnish much valuable information when properly used. Such tests have been made by a variety of methods. In some cases, the insulators under test have been placed in closed chambers into which salt mists have been injected. In other cases, the insulator surfaces have been artificially coated with dirt and then moistened by mists or light sprays of water. By making the test very severe, some idea can be obtained of the performance of insulators under the worst operating conditions which are probable.

With this in mind, a number of tests have been made on insulators of various types and sizes. Pin-type insulators, switch and bus insulators, and suspension insulator strings were included in the test. Both fog-type and standard suspension insulators were used. Post-type insulators were included among the various pin-type and switch insulators. The entire surface of each unit was coated with a mixture of salt and kaolin. This "dirt" was applied by spraying the surface with a concentrated solution of salt (sodium chloride) in water. The moistened surfaces were then dusted with powdered kaolin and allowed to dry. This process was repeated to build up, layer by layer, a heavy surface coating. Coatings of this type are very severe. The salt is readily soluble and so quickly produces a low resistance electrolyte in the presence of moisture. The kaolin acts as a binder to retard the removal of the salt during the process of testing and to increase the effective thickness of the moisture film.

After the application of the final layer of salt and kaolin, the insulators were placed in a dense mist of fine water drops, as produced by atomizer-type spray guns. The mist was directed so as to wet all surfaces of the insulators. Under these conditions, the 60-cycle flashover voltage was measured. Then the same insulators, still dirty, were placed in a light spray of water directed downward at about 45 degrees from the vertical, and simulating a light rain. The 60-cycle flashover voltage was again measured. In Figure 3, the lowest measured flashover voltage for each insulator tested is plotted against the insulator size as measured by its dry arcing distance. As would be expected, the test points fall within a band of considerable breadth. The breadth of this band is effected more by variations in the test than by variations in insulator types.

Table I

Kind of Dirt	Weight of Dry Dirt, Grams	Quantity of Distilled Water, Cubic Centimeters	Resistivity of Filtrate, Ohms Per Centimeter Cube	Approximate Parts Per Million in Solution
First Mixture				
Flint	5	50	21,000–23,000	28
Fly ash	5	50	1,930	320
Blast furnace	5	50	1,300–8,900	450–70
Railroad soot	5	50	592	1,000
Cement	5	50	236	2,500
Second Mixture				
	Using Dry Dirt Residue From First Mixture			
Flint	4.919	49.19	35,000–37,000	16
Fly ash	4.947	49.47	13,300	45
Blast furnace	4.916	49.16	5,000	120
Railroad soot	4.870	48.7	1,450	400
Cement				

In other words, the band may be used as a rough measure of the probable flashover voltage of any practical insulator under very severe service conditions. The clean-dry and clean-wet 60-cycle flashover voltages of the same insulators are plotted as bands in Figure 3 for comparison.

The great reduction in 60-cycle flashover occasioned by surface dirt makes it very difficult to find the relatively small differences which may result from variations in insulator design.

The same data is plotted in Figure 4 as a function of leakage distance. The breadth of band so obtained is even greater than when plotted against arcing distances. This reflects the loss in efficiency of leakage distance which is so often observed on insulators having a high ratio of leakage distance to arcing distance.

It is believed that, in general, the data summarized in Figures 3 and 4 are fairly representative of very severe operating conditions. As verification of this, a typical insulator was secured which had been in service in a very badly contaminated area near a chemical plant. This insulator was heavily coated with phosphates and miscellaneous industrial dirt, and represents an extreme practical condition.

It was tested with a minimum disturbance of the surface coating and the measured flashover voltage is plotted as a cross on Figures 3 and 4. Note that it falls within, but towards the bottom of the laboratory test band.

In the interpretation of laboratory flashover tests on dirty insulators, it is important that the element of probability be considered. Flashover on an insulator may be initiated by the chance simultaneous formation of several local sparks on different parts of the surface. The probability of this occurring on one insulator during the brief period of one test may be very remote. However, in service, with hundreds of insulators involved and for considerable periods of time, the probability of such an occurrence on some one unit is much greater. There is also the possibility that on continuously energized insulators in service, the heating and drying action of leakage currents will modify the flashover characteristics to some degree.

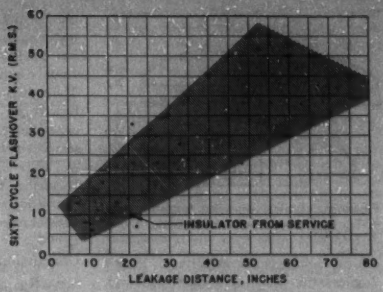

Figure 4. Sixty-cycle flashover voltage of dirty insulators of various types and sizes plotted as a function of leakage distance

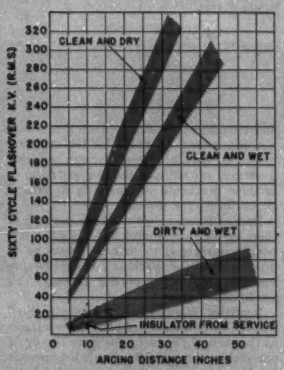

Figure 3. Sixty-cycle flashover voltage of dirty insulators of various types and sizes plotted as a function of arcing distance

Leakage Currents

To give some idea of the variations in leakage current which may occur across insulators under a variety of surface conditions, a number of laboratory measurements have been made. Figure 5 shows the variation in leakage currents experienced on a typical pin-type insulator of a size commonly used on 34.5-kv circuits. This type of insulator is illustrated in Fig. 6. The measurements on clean insulators were made with direct current in order to detect the small leakage currents which would otherwise be masked by the relatively large charging currents. Note that under dirty conditions in a mist, the leakage currents are of the order of one million times those which flow over a clean-dry insulator. Furthermore, in this particular case, no leakage measurements could be made on the dirty-wet insulator at voltages above 25 kv because at higher levels local sparking became severe, and variations in leakage current were so large and so rapid as to be unmeasurable by the method employed. Above 25 kv, some of the peaks of leakage current may have been ten times the level indicated by extrapolation of the lower test points.

With variations in leakage current of this magnitude, it is futile to try to control them by methods which are capable of changing the resistance of the leakage path by only a few per cent. Leakage currents can be effectively controlled only if some parts of the insulator can be kept relatively clean or relatively dry. Even then the clean or dry areas must be paralleled by an air gap which is wide enough to support the voltage without breakdown. If under the existing conditions, this cannot be accomplished, then steps must be taken to prevent burning of wood members by partially or totally bonding them with wire or metallic strips.[1]

Impulse Flashover

It has been considered generally that the impulse flashover of insulators would not be seriously affected by surface dirt. It is true that the impulse flashover of dirty insulators is not reduced as much as is the 60-cycle flashover. There is, however, an appreciable reduction in the

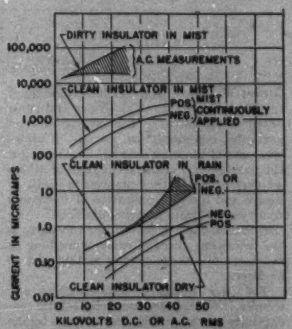

Figure 5. Leakage currents for a typical pin-type insulator under various conditions of surface contamination

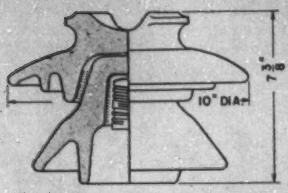

Figure 6. Typical pin-type insulator commonly used on 34.5-kv circuits

critical impulse flashover voltage. Figure 7 shows the result of impulse flashover tests on dirty $5^3/_4$-by-10-inch suspension insulators, using a $1^1/_2$-by-40 microsecond wave. The two upper curves show the critical impulse flashover voltage of clean dry insulators. The test points scattered along these two curves are the impulse flashover voltages measured on dry dirty insulators. The scattered points in the lower band are the measured impulse flashovers of dirty insulators in a fine mist of water. The variations in individual tests were quite wide, as indicated by the breadth of the band in Figure 7, and were too great to detect any significant difference between positive and negative wave values.

These impulse tests were made with the insulators coated with salt and kaolin, as described previously, and also with the insulators sprayed with a salt water mist. Since it takes an appreciable period of time to make one impulse flashover measurement, there is considerable washing action during the process of the test. It is, therefore, difficult to be sure that the degree of contamination remains the same during the test. Hence, even though an attempt may be made to produce a dirt film at the beginning of the test which approximates very severe service conditions, it is possible that the actual condition tested may be less severe. The values shown in Figure 7 may therefore be an optimistic estimate of those experienced under the worst probable service conditions.

This reduction in impulse flashover due to surface dirt may sometimes have to be considered in the selection of insulators to meet required basic insulation levels.

Conclusions

1. For operation in dirty areas, the most important consideration is the size of the insulator. Insulators, not one, but several steps larger than normal are often required. For example, it is not at all uncommon to use 34.5-kv or even 46-kv insulators on 13-kv circuits in dirty areas.

2. All American standard insulators are made with surface contamination as one of the major design factors. For most applications, in dirty areas, the best balanced insulation will be obtained by the use of standard insulators of adequate size. There are cases where the use of standard units of adequate size becomes impractical due to clearance limitations or to other local conditions. In these cases, special designs, usually called fog-type insulators, may be required.

3. In dirty areas, wood members should be bonded, and conducting insulator pins should be used to prevent burning.

4. In very dirty areas, it may be necessary to wash insulators periodically[3] in order to maintain service.

5. Impulse flashover voltages may be seriously affected by surface dirt. This influences the ability of insulators to meet required basic insulation levels in service.

6. Predetermination of the proper insulation level is at present almost impossible except through experience with a particular exposure. There is no yardstick to measure

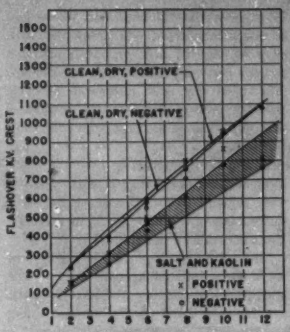

Figure 7. Impulse flashover of dirty $5^3/_4$-by-10-inch suspension insulators

the severity of conditions in any location. The factors entering into such a measurement are:

a. The rate of deposit of dirt.

b. The nature of the dirt—its relative conductivity and its bonding properties.

c. The frequency and intensity of rain and wind.

There is real need for a study of these factors to establish a relative measure of exposure severity and to permit intelligent predetermination of required insulation levels.

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Discussion

Grover W. Lapp (Lapp Insulator Company, Inc., LeRoy, N.Y.): This paper and three others contributed to this symposium on insulator contamination, present a useful summary of the work that has been done for the past several years on the basis of which we may expect further progress in insulator design and specific application of insulators to service in contaminating districts.

One fact upon which all seem to agree is the beneficent effect of rains or washing in reduction of leakage current and liability to flashover.

The dry insulator seldom gives any trouble. Wetness produces electrolytic conductivity to various degrees as shown by the data. Fortunately, in most cases the conductive ingredient in the contaminated coating is soluble and can be reduced by leaching out. The soluble part of the coating goes into solution when wetted, and, as the moisture dries, a point of concentration is reached at which saturation takes place after which there is a successive deposition of crystals and salts of the various soluble materials, producing a more or less tightly adherent coating cemented to the insulator surface by many wettings and dryings and reduced by wind and weather not readily reproduced in laboratory tests.

So beneficial is water, and especially rain water, in leaching out this soluble conductive adhesive part of the coating that tests reported after artificial washing show a reduction of leakage current to only ten per cent of the value before washing.

There may be instances in which sufficient surface on the insulator can be maintained dry to prevent flashover. Such secluded surfaces are likely to produce bulky designs and are difficult to wash. Even if rain cannot get access to these hidden surfaces, humid air on a cool insulator can condense moisture, and drifting fog can wet them to produce maximum conductivity at critical times during the seasons.

Energy losses help to dry up surfaces at short radius, as around the pin of a suspension insulator, to such an extent as to produce frequent sparking. Unsatisfactory voltage gradients reported indicate this condition.

Insulator manufacturers formerly listed wet flashover distance of pin-type insulators as the sum of the distances from the lip of one shell to the next, plus the radial distance to the pin. At an early stage of voltage rise on a flashover test, these dry gaps spill over and become valueless or even a liability. From that point up, the portions of

wetted path in series with these arclets are the principal elements to prevent flashover. On the other hand, it is difficult to cause flashover along a uniformly wetted path from terminal to terminal. Under ideal conditions this uniform conductive path serves to make the potential gradient more uniform in that locality so that flashover may occur elsewhere.

For these reasons, and in most cases, it is best to plan on uniform wetness of path as a principle of design. Examples of such designs have served well, notably old transformer bushings. In more recent years post-type porcelain insulators having many small externally exposed corrugations have done well in service.

There is some prospect of specific improvement in design for self-conditioning insulators to be used in contaminated atmospheres, so that we may be free from the blanket rule that two inches of leakage distance must be provided per kilovolt of applied voltage.

H. A. Frey: Lapp's comments, concerning the beneficial washing affects of rain, are certainly quite true. We must realize, however, that these natural washing actions require time. When rain water initially wets the surface of a dirty insulator the first action which occurs is detrimental. The moisture takes into solution some of the soluble matter on the surface and forms a rather highly conducting film. The insulator must be capable of operating satisfactorily under these conditions and until the washing action can be completed. For this reason, insulators are provided with hoods or corrugations. These provide additional leakage distance and some protected area which is capable of supporting line voltage until exposed surface can be washed to some degree. The protected leakage surfaces must not be short circuited by narrow air gaps, otherwise the shunting gaps will break down and the value of the theoretical leakage distance is lost. Obviously, the most effective relationship between the length of the protected leakage distance and the length of the shunting air gap is determined in some measure by the physical arrangement of the air gaps. Leakage distance, as measured on paper, is of little value if it is short circuited in service at the time when most needed. Perfectly smooth suspension insulators having relatively low leakage distance but ample spacing have been used with good results under some contamination conditions.

Coupling Capacitors and Traps for Rural Line Carrier

AN AIEE COMMITTEE REPORT

AT THE January 1947 meeting of the Institute, two technical papers[5,6] were presented which described in some detail the design and the method of application of equipment utilized to provide rural telephone service by carrier on power lines. In addition, there have been numerous articles[5,6] describing the system in the technical press. Subsequent to the time these papers were presented, carrier telephone equipment had been installed on rural power systems throughout the country, establishing carrier telephone systems on a commercial basis. This equipment has also been applied to telephone circuits to provide subscriber service and short-haul toll service.

Since the application of the carrier telephone system to power lines involves the attachment of newly developed equipment of special design to primary power conductors, there has naturally been considerable interest evinced in the performance of the equipment in so far as its effect on operation of the power systems is concerned. With this in mind, the carrier current committee of the Institute obtained information on the performance of the carrier telephone equipment which has been installed on power lines during the past year. The purpose of this report is to discuss the performance of this equipment and to provide information on the present status and probable future field of use of the rural carrier telephone system. Since details concerning operation of the carrier system are provided in numerous articles and papers[1-6] only a brief description of the essential features will be included here.

The system operates at frequencies in the 150–425 kc band. Five 2-way channels, each serving up to eight subscribers, can be obtained by utilizing the entire band. The subscriber terminal equipment consists of a carrier transmitter and receiver operated from the 120-volt power service. The handset provided for the subscriber terminal is similar to a conventional handset, and, as far as the subscriber is concerned, the operation of the system is identical to a voice frequency telephone circuit. The common terminal unit is provided to connect the power line carrier section to the central office. It consists of a carrier transmitter and receiver, terminating in an ordinary voice frequency pair which provides the connections to the central office. Each channel used in a carrier section requires a separate common terminal unit.

The system is designed for use primarily on power lines of the multigrounded neutral-type operating at or below 8,700 volts to ground. It can be extended to other types of systems under some conditions. To apply the system to power circuits, it is necessary to provide series chokes to confine the carrier frequencies to the section of power line utilized, and coupling capacitors to couple the subscriber and common terminals to the power circuit. Figure 1 shows a typical layout of a power line modified for carrier telephone service. It can be seen that three types of chokes, having characteristics as shown in Table I are used. The chokes provide the necessary value of impedance at carrier frequencies to isolate or reduce the carrier transmission beyond them, but at the same time their 60-cycle characteristics are such that they have a minimum effect on the operation of the power system. The "isolating" choke is shown in Figure 2. It is mounted in a 2-bushing oil-filled metal container and is protected by gaps to ground and across the coil. It is designed and constructed so as to meet all of the dielectric strength requirements for 15-kv class equipment of ASA-C57.1.[7] The "transmission" and "tap" chokes are designed for mounting on pin-type insulators of the same type as used on the power circuit on which they are applied. Therefore, the dielectric strength to ground is provided by the insulators. Gaps are provided across the coils to by-pass surge voltages. The more important tests to which the chokes are subjected are shown in Table II.

The coupling capacitors presently employed are glass encased, hermetically sealed, mineral oil impregnated, paper dielectric units. Figure 3 shows one of these units. They were designed to meet the requirements of AIEE Standard Number 31[8] and for the 15L rated insulation class. A fuse of low ampere rating is used in the connection of the capacitors to the phase wire.

The capacitors are given extensive tests to insure that they meet requirements. The types of tests and corresponding requirements are summarized in Table III.

Where this is not feasible, the capacitors should be located as near as possible to a transformer pole. For this latter condition, some of the protection afforded by the lightning arrester or gap is sacrificed.

Coupling units are used in the ground side of the capacitor. They provide the 60-cycle drainage to ground and carrier connection to the terminal equipment. The normal 60-cycle voltage drop across the coupling unit is less than one volt. It is protected for abnormal voltages by copper protector blocks which break down at about 1,200 volts.

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Personnel of the AIEE carrier current committee: L. F. Kennedy, *chairman;* S. C. Bartlett, W. H. Blankmeyer, C. W. Boadway, J. M. Dunham, L. C. Eaton, R. C. Ericson, L. F. Ferri, W. J. Googe, K. E. Hapgood, E. W. Kenefake, S. C. Leyland, S. Lubin, L. E. Ludeksna, D. M. MacGregor, T. W. MacLean, R. H. Miller, R. E. Neidig, F. M. Rives, Merle Robison, Trueman T. Smith, Oliver A. Starcke, T. D. Talmage, and Mead Warren, Jr.

The committee desires to express its appreciation for the assistance rendered in the preparation of this report by the chairman and members of the Edison Electric Institute–Bell System Joint Subcommittee on rural telephone service by carrier on power circuits.

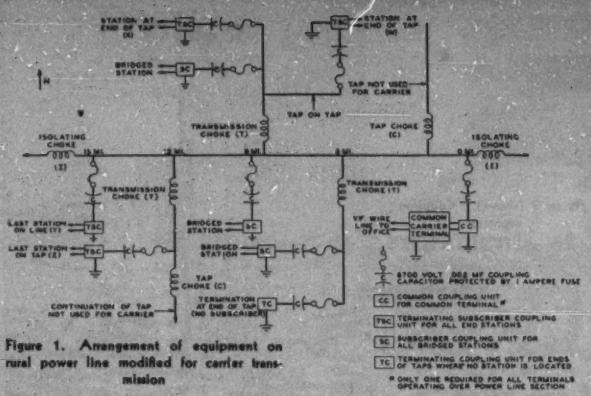

Figure 1. Arrangement of equipment on rural power line modified for carrier transmission

The chokes, capacitors, and coupling units are all installed on the poles in accordance with the requirements of the National Electric Safety code for similar power line equipment. Figure 4 shows a typical installation of some of this equipment.

All of the carrier equipment is normally owned by the telephone company. However, all equipment installed on the power line that is attached to the power circuit (chokes, coupling capacitors, and coupling units) is installed and maintained by the power organization. Equipment not connected directly to the power circuit (telephone drops and connections to the coupling units) is installed and maintained by the telephone company.

Field experience to date indicates substantial freedom from maintenance difficulties with the carrier equipment connected to the power line or interruptions to power service caused by it. Up to April 1, 1948, 14 capacitor failures have been reported out of a total of around 4,000 installed. Most of the failures were experienced during the severe lightning season last summer. In all of these cases, the capacitor fuse operated without causing operation of other system protective devices, so no interruptions to power service resulted. Six cases of capacitor fuse failures have been reported which did not involve failure of capacitors. Most of the capacitors which have failed were of the earlier manufacture and since that time test procedure changes have been made which, it is expected, will improve the capacitor performance. Also additional studies and tests of the capacitors and methods of

Table I. Summary of Characteristics of Chokes Installed on Power Lines

Type of Choke	Maximum Continuous Current, Amperes	One Second Short Circuit Current Rating, Amperes	Nominal Inductance, Millihenrys	60-Cycle Impedance, Ohms	Temperature Rise with Maximum Continuous Current, Degree C	Impulse Breakdown Voltage of By-Pass Gap Kv	Weight Pounds
Isolating*	15.0	500.0	10.0	0.9+j4.5	85	30**	60
Tap	7.0	500.0	2.5	0.4+j0.95	55	21	12
Transmission	9.0	500.0	0.6	0.2+j0.23	55	21	8
	10.0	500.0	0.3	0.1+j0.12	55	21	7

*Minimum withstand requirements of isolating choke assembly with case grounded are 35 kv dry for one minute, 30 kv wet for ten seconds and 95 kv on impulse.

**In addition to 3/8 inch by-pass gap, gaps with electrode spacing of approximately 2 1/2 inches are connected between each bushing and ground. These gaps are protected by insulating shields and break down at approximately 75 kv.

Table II

Type of Test	Isolating Choke, 10-Mh Coil	Requirement: Tap Choke, 2.5-Mh Coil	Transmission 0.6-Mh Coil	Choke, 0.3-Mh Coil
Routine Tests				
D-c resistance, ohms (max.)	0.9	0.4	0.2	0.15
Inductance at 200 kc, mh		2.6–4.5	0.53–0.61	0.27–0.31
Ins. resistance to case	1,000 megohm, Min.			
Dielectric strength, 60 cycle	34 kv			
Sampling Tests				
Impulse withstand to case, kv*	95			
Impulse withstand across coil, kv*	55	30	30	30
One-second, 60-cycle current withstand, amperes	500	500	500	500
Nominal continuous 60 cycle	10	4	4	4
Maximum continuous 60 cycle for 40 degree centigrade ambient	15	7	9	10

*On 1 1/2-40 wave with gaps blocked.

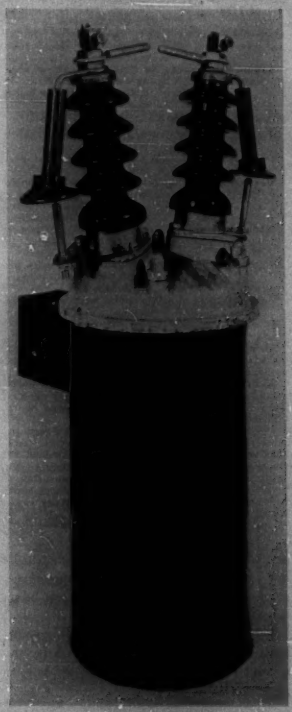

Figure 2. Isolating choke

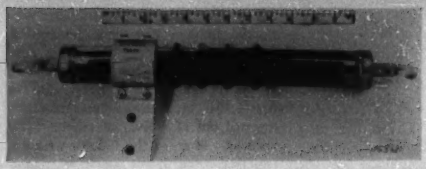

Figure 3 (above). Coupling capacitor

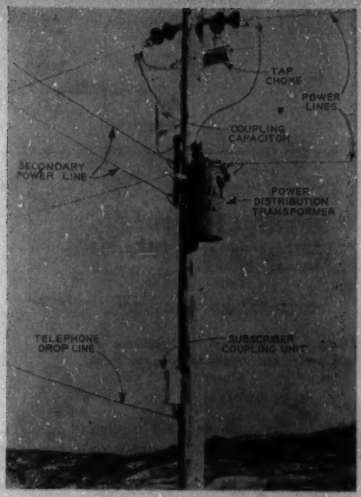

Figure 4 (right). Typical installation on power pole

installation have been initiated which should lead to still further improvement.

Two isolating choke coil failures have been reported, one of which caused an interruption to power service beyond the point where it was installed. The failure occurred in a choke of early manufacture and was traced to a defect in design which has been corrected. Circumstances regarding the other failure have not yet been reported.

Table III

Type of Test	Requirements
Routine Tests	
Dielectric strength	Withstand 18 kv, 60 cycles for one minute
Capacitance (at 60 or 1,000 cycles)	1,950 micromicrofarad minimum: 2,450 micromicrofarad maximum
Insulation resistance	Not less than 10,000 megohms at 25 degrees centigrade
Liquid tightness of container	No visible evidence of leakage after three hours in oven at 75 degrees centigrade
Sampling Tests	
Impulse strength	Withstand 95 kv, $1^1/_2$–40 wave
60-cycle dry	Withstand 35 kv for at least one minute*
Resonance	Tests at 100 and 600 kc to insure that capacitance is between 1,800 micromicrofarad at such frequencies
Radio frequency noise	Free from radio frequency noise when subjected to 10,000 volts, 60 cycles
Life test	Withstand 13,000 volts for 1,000 hours at 65 degrees centigrade
Seal test	Withstand without damage extensive heat cycling and thermal shock tests

*Made on routine basis on earliest manufacture only, later changed to avoid possibility of corona damage on units shipped to customers.

Capacitors of this design have withstood impulse voltages considerably in excess of test requirements. They have also lasted for several thousand hours on accelerated life tests.

While the coupling capacitors are designed to meet the same, or more stringent, dielectric tests than are required for other equipment installed on the power circuits, it is intended that they be installed on the same pole as distribution transformers so as to take advantage of the protection afforded by lightning arresters and/or gaps normally associated with transformers.

Other difficulties with the carrier system have been encountered which did not involve equipment connected to the power circuit. These difficulties include some interference with broadcast radio reception which has been cleared up by minor changes in the design of carrier terminal equipment. There have been some other equipment difficulties which are being given intensive study and it is anticipated they will be corrected in the near future.

The results of operation of the rural carrier telephone system on a substantial number of rural power systems for periods of time ranging from over a year to a few months indicate that the design and construction of equipment applied to power circuits is basically satisfactory. It is apparent that the system no longer is developmental but can be considered and utilized as a practical system in lieu of, or in conjunction with conventional telephone circuit facilities. The field of use of the system in the future will be determined by its relative economy and suitability in comparison to other types of facilities, and will depend on a number of factors which will need to be evaluated in specific situations. It will be most attractive where the prospective telephone customers served by power distribution lines are remote from existing telephone facilities, and where the separation between customers is of the order of one-half mile or more. The relative flexibility of the carrier system deserves particular consideration. When a power line has once been equipped for a carrier section, service can be rendered to any customer within the section up to the ultimate capacity of the system, usually 40, by adding channels from time to time without necessarily making any modifications in the sectionalizing equipment. In the event of discontinuance of service by carrier to a customer or on an entire section of power line, the recovery of original investment in equipment is much greater than in the usual case with conventional facilities.

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No Discussion

Report on Method of Measurements at Carrier-Current Frequencies

AN AIEE COMMITTEE REPORT

THE purpose of this report is to outline certain methods of measurement to obtain data for the application of carrier-current apparatus to power lines. The objectives in regard to the data needed have been set down in a report, Study of Power System Characteristics at Carrier Frequencies. This report will be used as a guide as to the desirable data to be accumulated and hence sets the pattern for the type of measurements to be made.

In general, it has been felt desirable by the committee that these measurements should be made over an extended frequency range of 30 to 300 kc, and, wherever possible, data over this wide range should be obtained. Undoubtedly, there will be instances where equipment is not available to cover this wide range and the data for a narrower range will have to be taken. This information will be valuable even though it only covers the 50 to 150 kc range or some other range similar to this.

In investigating this general problem of obtaining data, it becomes quite obvious to anyone that it would be nice to accumulate a large amount of varied information. It is also obvious that if an ambitious program is set up, little may be accomplished. It seems that the field would be served best by information on effect of connected power apparatus and noise levels.

Referring to the previous report on Study of Power System Characteristics at Carrier Frequencies, the following tabulation will give the items referred to in this report and a suggested method for obtaining the data in each case. In this report are five methods of measurements, covered under Part I to Part V inclusive.

A. Substation Equipment

a. Power Equipment. This covers power transformers, capacitor potential devices, instrument transformers, coil circuit breakers, lightning arresters, and bus structures. Information is most desired on the first two items, and this can be obtained by using one of the methods of measurements described in Part I. In other words, if the impedances of these devices are known, their effects on carrier frequencies can be determined.

b. Carrier Equipment

1. Line Traps. Impedance of line traps can be obtained by the method of measurement described in Part II and if more detailed impedance characteristics are required, the method described under Part I may be used.

2. Coupling Capacitors. The impedance of coupling capacitors can be measured by the method described in Part I to yield information on power factor. This measurement should be made with and without protective equipment connected, and the high-voltage side of the capacitor should be grounded in the setup so that the low side will be at high (radio-frequency) potential simulating the actual usage of the capacitor.

3. Line Tuning. The pass band characteristics of line tuning unit can be obtained by the method of measurement described in Part III and may be for a simple or a complex tuning unit.

c. Complete Substations. It would be interesting to obtain transmission characteristics on complete substations by the use of the method in Part III. However, it would be just as desirable to measure the impedance of the substation by the method described in Part I.

B. Transmission Lines

a. Depending upon the circumstances, either by the method described in Part III which is applicable to long lines, or the method described in Part IV for short lines, the impedance and attenuation should be measured on certain typical lines and cables. This should bring out the effect of ground wires and differences between various phase wires.

b. More information is needed on the voltage distribution among the phase wires when coupled phase-to-ground and interphase. Information on the field strengths in the vicinity of power lines for interphase and phase-to-ground connection would be desirable.

Part I. Method of Measuring Unknown Impedances Which Contain Reactance and Resistance

General

In order to measure impedances of various kinds of connected apparatus to power transmission lines, it is expected that these impedances will involve both reactance and resistance, and the methods described subsequently will give desirable schemes for measuring these kinds of impedances. In general, the problem of measuring an unknown impedance can be done by a Q-meter or an r-f bridge, but, because there seems to be no suitable r-f bridge, the Q-meter method will probably have to be used. This method is described in detail in the instruction book for Boonton Q-meter[1] for measuring an unknown impedance and need not be duplicated here. In very many cases, a Q-meter will not be available but the principles of this type of measurement can be used. Certain liberties can be taken because of the low frequency range, and a method can be used which uses equipment generally found around carrier-current installations. Therefore, when a Q-meter is not available, unknown impedances can be measured with the following equipment:

1. Carrier-current transmitter or r-f generator.

2. Resistors.

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Personnel of the AIEE carrier current committee: L. F. Kennedy, *chairman;* S. C. Bartlett, W. H. Blankmeyer, C. W. Boadway, J. M. Dunham, L. G. Eaton, R. C. Ericson, L. F. Ferri, W. J. Googe, K. E. Hapgood, E. W. Kenefake, S. C. Leyland, S. Lubin, L. E. Ludekens, D. M. MacGregor, T. W. MacLean, R. H. Miller, R. E. Neidig, F. M. Rives, Merle Robison, Trueman T. Smith, Oliver A. Starcke, T. D. Talmage, and Mead Warren, Jr.

Personnel of the project subcommittee: E. W. Kenefake, *chairman;* C. W. Boadway, and T. D. Talmage.

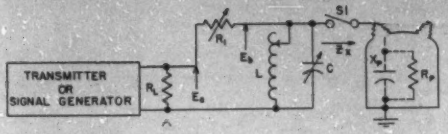
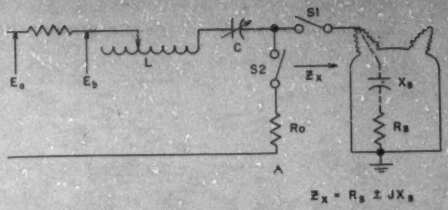


Figure 1. Parallel connection for impedance measurements

Point A also grounded

$$Z_x = \frac{X_p^2 R_p + JR_p^2 X_p}{R_p^2 + X_p^2}$$



$$Z_x = R_s \pm JX_s$$

Figure 2. Series connection for impedance measurements

Point A also grounded

3. High Q-coil and low power-factor calibrated capacitor.

4. Vacuum-tube voltmeter.

As in the case in any measurement procedure, lead lengths and proximities should be watched very carefully.

Description

Figure 1 indicates a method which is basically a substitution method (similar to the parallel connection method with the Q-meter) for measuring an unknown impedance consisting of resistance and inductive reactance or capacitive reactance. By using a substitution method, many unknown factors cancel out, such as Q of coils, wave form of source, and so forth. The only requirement is an accurate calibration or measurement of the values of C and R. The latter should be noninductive and have little distributed capacity.

The purpose of R_L is to provide a firm source of r-f voltage of E_a which will not vary too much with changing impedances. This may not be too essential. The series resistance R_1 is to provide an output of constant-current character in order to detect resonance of the circuits by watching E_b. L could be a tuning unit coil normally used to resonate coupling capacitors. C could be a mica decade capacitor box from 0.0001 to about 0.1 microfarad.

Method of Measurement Parallel Connection Method (Figure 1)

1. Load the transmitter or generator into R_L so that about maximum E_a is obtained and it is of good wave form at the frequency desired.

2. Connect the rest of the circuit with S_1 open, and resonate the circuit by adjusting L and C for a maximum value of E_b. R_1 should be kept reasonably large so that a fairly sharp indication of resonance is obtained. Either L or C must be greater than the equivalent of L or C of X_p in the unknown impedance. Note the value of E_b and C.

3. Close switch S_1, and resonate by varying C. If C has to be diminished, then X_p is of capacitive reactance and vice versa for inductive reactance. Also, if resonance cannot be obtained with zero capacity at C, then step 2 will have to be repeated with a larger value of C and a lower value of L. When step 3 is complete, note new value of E_b and C. If E_a varies, this information must also be kept. The equations given after Figure 1 and in the following will indicate the method of calculating the elements of the unknown impedance.

E_{a1}, E_{b1}, C_1, and Q_1 = readings with $S1$ open.
E_{a2}, E_{b2}, C_2, and Q_2 = readings with $S1$ closed
C_1 and C_2 are read from C. Q_1 and Q_2 are calculated.

$$Q_1 = \frac{E_{b1}}{E_{a1}\ E_{b1}} \times \frac{R_1 C_1 f}{1.59 \times 10^8}$$

$$Q_2 = \frac{E_{b2}}{E_{a2}\ E_{b2}} \times \frac{R_1 C_2 f}{1.59 \times 10^8}$$

$$Q_x = \frac{Q_1 Q_2}{Q_1 - Q_2}$$

Where Q_x is the unknown impedance

$$C_p = C_1 - C_2$$

If $(C_1 - C_2)$ is negative, then X_p is inductive and

$$L_p = \frac{2.53 \times 10^{10}}{f^2 (C_2 - C_1)}$$

$$X_p = \omega L_p = \frac{1}{\omega C_p} = \frac{1.59 \times 10^8}{f(C_2 - C_1)}$$

$$R_p = \frac{Q_x}{\omega L_p} = Q_x C_p \omega = \frac{1.59 \times 10^8 Q_1 Q_2}{f C_1 (Q_1 - Q_2)}$$

Series Connection Method (Figure 2)

This method should be particularly useful where impedances are of very low values and not particularly suited to the method described for the parallel connection. It consists of using the same elements by putting the unknown impedance in series with the resonant circuit instead of in parallel with it. It is expected that this method will not be used as much as the one above. Figure 2 indicates the connection of the apparatus and step 1 is identical with step 1 in the previous case. R_0 could be a noninductive resistor of some convenient size so that, for the series resonant case, E_b does not become too low for accurate measurement.

After the transmitter is loaded into R_L, close $S2$, open $S1$, connect the whole circuit to R_L and resonate L and C. The use of R_0 complicates the calculation somewhat, but it may be necessary in order to keep E_b from being so small that inaccuracies arise from stray pickup in the voltmeter leads. If the measurements can be made with R_0 short-circuited, it would be desirable. At resonance, note values of E_a, E_b, and C.

Open $S2$, close $S1$, resonate the circuit by watching for a minimum E_b and note new values of E_a, E_b, and C. The following equations will indicate the method of calculating the unknown impedance.

$$Q_1 = \frac{1.59 \times 10^8}{C_1 R_1 f} \times \frac{E_{a1} - E_{b1}}{E_{b1}}$$

$$Q_2 = \frac{1.59 \times 10^8}{C_2 R_1 f} \times \frac{E_{a2} - E_{b2}}{E_{b2}}$$

$$Q_x = \frac{(C_1 - C_2) Q_1 Q_2}{C_1 Q_1 - C_2 Q_2}$$

Where Q_x is the unknown impedance

$$C_s = \frac{C_1 C_2}{C_2 - C_1}$$

If $C_2 - C_1$ is negative, then X_s is inductive and

$$L_s = \frac{2.53 \times 10^{10} (C_1 - C_2)}{f^2 C_1 C_2}$$

$$R_s = \frac{\omega L_s}{Q_x} = \frac{1}{\omega Q_x C_s} = \frac{1.59 \times 10^8 \left(\frac{C_1}{C_2} Q_1 - Q_2 \right)}{f C_1 Q_1 Q_2}$$

$$X_s = \omega L_x = \frac{1}{\omega C_x} = \frac{1.59 \times 10^8 (C_1 - C_2)}{f C_1 C_2}$$

Conclusions

The parallel method of measurement gives the impedance as effective parallel elements and the equivalent series elements will have to be calculated. For the parallel case:

$$Z_s = \frac{X_p^2 R_p + J R_p^2 X_p}{R_p^2 + X_p^2}$$

Therefore:

$$R_s = \frac{X_p^2 R_p}{R_p^2 + X_p^2}$$

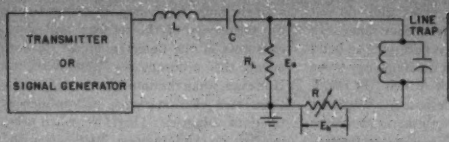


Figure 3. Circuit for measuring line trap impedances

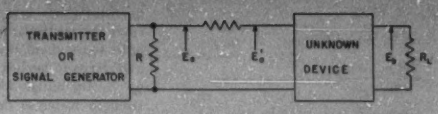


Figure 4. Circuit for measuring insertion losses

$$X_s = \frac{R_p^2 X_p}{R_p^2 + X_p^2}$$

To be comparable with series connection measurements where

$$Z_s = R_s + JX_s$$

If the results give Q_s a value greater than ten, then

$$X_s = X_p$$
$$L_s = L_p$$
$$C_s = C_p$$
$$R_p = Q^2 R_s$$

The units to be used for formulas in these two methods are:

R = resistance in ohms
X = reactance in ohms
L = inductance in microhenries
C = capacitance in micromicrofarads
f = frequency in kilocycles per second

The following is a suggested set of data to obtain: f; E_{a1}; E_{b1}; C_1; E_{a2}; E_{b2}; C_2; $(C_1 - C_2)$; and Q_s.

The rest of the values of the unknown impedance can be calculated as desired.

Both methods described above assume the capacitor C has zero power factor or infinite Q. Actually, the results will be quite accurate if C is made up of good mica capacitors which have Q's of 1,000 to 10,000.

Part II. Method of Measuring Impedance of Line Traps

1. The impedance of a single-frequency trap can be obtained best by calculation when f, L, and Q are known. If Q is not known, then it can be determined by a single-frequency measurement with the method described subsequently.

2. The impedance of 2-frequency traps can be found easier by measurement as follows (single-frequency traps can be measured this way also). (Note Figure 3).

$$Z = \frac{E_a}{E_b} R$$

Use vacuum tube voltmeter for E_a and E_b.

The foregoing formula is accurate enough if E_a/E_b is large enough, particularly for off-resonance conditions of line trap. By adjusting R, keep E_a equal to $10E_b$ if at all possible.

The reason for L, C, and R_L is to insure as nearly a pure sine wave of the signal as possible across R_L. Therefore, L/C should be high.

This gives the total impedance and does not evaluate the resistance and reactance components.

Part III. Method of Measuring Transmission or Insertion Losses

It is often desirable to know the insertion loss of a given device when connected into a circuit or transmission line. These transmission losses may be observed over a narrow band of frequencies or over a broad band of frequencies, as in the case of certain transformers and transmission lines or cables. This method is useful and accurate when the insertion loss is in excess of ten decibels. It is particularly useful in determining the attenuation of transmission lines which are relatively long and determining effects of other lines on this attenuation. The same method can be used to determine the transmission characteristics of tuning units and 2-frequency carrier telephone channels.

In the diagram shown in Figure 4, R_s should be equal to the input impedance of the unknown device, and R_L should be equal to the output impedance of the unknown device. R_s is determined by adjusting E_a for half the value of E_a. R_L is normally adjusted for maximum power into this resistance and will give the proper match at the other end. R_s and R_L should be adjusted for some normal operating point and left fixed over the frequency range to be studied.

Readings should be taken of E_a and E_b, and a curve should be plotted which gives the decibel differential between the two voltages. In addition, six decibels should be subtracted from this differential to take into account the voltage loss in R_s when there is no insertion loss.

The use of R_s gives a method which approximates measuring input and output power.

Part IV. Method of Determining the Characteristics of Transmission Lines and Cables by Means of Open and Short-Circuit Measurements

Where transmission lines or cables are relatively short and the loss is less than ten decibels, the transmission measurement described in Part III becomes a little inaccurate. The type of measurement described here involves more calculation, but greater accuracy can be obtained on values of attenuation.

By means of a method of measuring impedance described in Part I, the open- and short-circuit measurements can be made from which calculations will give characteristic impedances, loss per unit length, and speed of propagation.

From the method in Part I the value obtained for open-circuit impedance is:

$$Z_{oc} = R_o - JX_o$$

The value for short-circuit impedance is

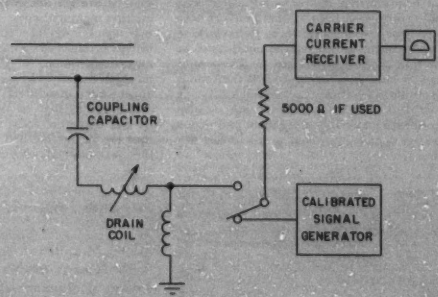


Figure 5. Circuit for measuring noise and interference

$Z_{sc} = R_s + JX_s$

Solve for A and B from

$$A = \frac{R_oR_s + X_oX_s}{R_o^2 + X_o^2}$$

$$B = \frac{R_sX_o + R_oX_s}{R_o^2 + X_o^2}$$

Then

$$\tanh 2\alpha l = \frac{2A}{1 + A^2 + B^2}$$

$$\tanh 2\beta l = \frac{2B}{1 - (A^2 + B^2)}$$

By evaluating to determine α, the attenuation per unit length of line can be determined.

The preceding equations come from the following relations:

$$\tanh \gamma l = \tanh (\alpha + j\beta) = \frac{Z_{sh}}{Z_{oc}} = A + JB$$

Also

$$Z = \sqrt{Z_{sc}Z_{oc}} = \text{characteristic impedance.}$$

Part V. Method of Measuring Noise and Interference

In order to measure radio-frequency noise and interference in the carrier band of frequencies, it is necessary to have some kind of electronic voltmeter which will read the voltage. Field-strength instruments are available for measuring these quantities, but in general they do not cover this particular frequency range. In order to use them, it is necessary to build a converter to convert the frequency range up to the range for which the field-strength meter is designed. Wherever possible, a field-strength meter should be used for measuring noise and interference.

In view of the fact that field-strength meters are likely not to be too plentiful in the field, it is possible to get reasonably reliable data by means of the substitution method using a carrier-current receiver. This receiver should be a receiver designed for reception of voice-modulated signals so that it has a band width of approximately 6 kc, and fairly good selectivity. The receiver should employ four or more tuned circuits in order that the selectivity will be reasonably good.

With the receiver connected through a coupling capacitor to the line as shown in Figure 5, noise or interference will produce a detector voltage or current which can be noted. Then the receiver is switched to a calibrated signal generator, and the same reading on the detector meter is obtained by adjusting the output of the signal generator. The output of the signal generator is then equal to the voltage received from the line. If a calibrated signal generator is not available, a suitable generator (transmitter) and attenuator together with a vacuum-tube voltmeter can be used.

When measuring interference, which is a single radio frequency, in this manner the selectivity of the receiver and time constants of the detector circuit do not affect the accuracy of measurement. However, when random noise is measured, these two characteristics will influence the data obtained. Therefore, when taking data on random noise, information on selectivity such as a complete curve or a figure on the frequency where the response is 3 decibels down together with the number of tuned circuits should be made part of the data. Also, the complete information on the detector circuit as to the type of detector and time constants should be included.

The receiver should be operated near the coupling capacitor so that no transformer or cable is in between, which would bring inaccuracies with regard to impedance levels and so forth.

The receiver should be of high input impedance or decoupled (to prevent loading) from the power line by a 5,000-ohm resistor.

In this way, the data will more nearly represent the noise voltage on the power line itself and give a better base for comparing data from different locations. The coupling capacitor should be resonated by a tuning coil at the frequency for which the receiver is tuned.

Reference

1. Boonton Q-Meter Instruction Book. Boonton Radio Corporation, Boonton, N. J.

Discussion

A. A. Sackler (Ebasco Services Incorporated, New York, N. Y.): With the co-operation on the part of the men in the field, the recommendations embodied in this report by the carrier current committee should make enough carrier current data available to take much of the guess work out of the installation of carrier current channels.

Now that the methods of making carrier current tests on transformers, capacitor potential devices, instrument transformers, and so forth have been standardized the next logical step would seem to be to have these measurements made on the equipment at the time of their manufacture. This would provide much more accurate data than could be obtained from field measurements, for at their best field measurements are usually made with make-shift equipment and under adverse conditions which result in low accuracy.

Naturally, measurement of transmission lines, cables, and busses must be made in the field. In this connection it may be of help if measuring procedures were outlined in more detail to allow for better comparison of data. An example of this would be definite information as to whether unused phases should be grounded or left floating when making measurements or whether it is advisable to make measurements for both these conditions.

The authors have also stated that no suitable r-f bridges are available at present for making measurements. We have had some experience in using a simple homemade r-f bridge in making characteristic impedance measurements on a 132-kv cable. The methods described in parts III and IV of the paper were also used. While some discrepancies existed in the results obtained by the three methods, it is quite possible that the bridge method was as accurate as the two methods described by the committee. It would seem worthwhile to further investigate the possibility of using r-f bridges for making measurements.

In part III, describing the method of measuring transmission and insertion losses, it should be noted that this method is also useful for measuring the characteristic impedance of lines as well as measuring losses. The magnitude of the impedance is obtained without the necessity of making any calculations.

In part III of their paper, the authors also recommend that the values of R_s and R_l be left fixed over the frequency range to be studied. However, in many cases the impedance of the devices may vary appreciably with frequency, in which case inaccuracy will result due to the impedance mismatch. In these cases it may, therefore, be necessary to readjust R_s and R_l at several points in the frequency spectrum under consideration.

Simplified Calculation of Fault Currents

AN AIEE COMMITTEE REPORT

Synopsis: A simplified procedure for the calculation of short-circuit currents was presented in earlier reports[1,2] which has been found to be generally satisfactory. It is intended for general use by the industry as a simplified method of approximating the magnitude of fault currents; however, other more rigorous methods should be used where required.

THE PRECISE determination of short circuit currents involves a complicated calculation which in many instances is not necessary. Certain approximations can be made although a degree of judgment must be applied in any simplified method. The method presented here is based upon the determination of an initial value of rms symmetrical current with which multiplying factors are used for application purposes. It may be used, where applicable, in place of the methods which involve the use of decrement curves. It is believed that this procedure is sufficiently accurate to serve as a reliable basis for the usual application of interrupting devices and as a preliminary basis for relay settings. A recent review of the procedure revealed the need for certain modifications in order to improve and bring the previous report up to date.

The multiplying factors given in Tables I and II take account of generator decrement and the asymmetrical value of current as influenced by the system d-c time constant, and they are adjusted to make allowance for the increase in generator excitation required to maintain normal terminal voltage under load conditions. They neglect, however, such influences as generator voltage regulators and phase-angle differences between generator rotors, which have but little effect within the usual operating time of modern circuit breakers.

A comparison of the current magnitudes determined by these factors with those determined from decrement curves[4] previously in general use is given in Figure 1. The time intervals selected for comparison correspond to power circuit breaker interrupting time ratings of 2, 3, 5 and 8 cycles, with time from inception of fault to parting of contacts assumed to be not less than 1, 2, 3, and 4 cycles respectively. The fault current and reactances are based on the system kilovolt-amperes. The earlier "decrement curves" gave rms total current, but for purposes of comparison, Figure 1 also includes "a-c component only." The total rms current (decrement curve method) is based on the assumption that for all short circuits the decay of the d-c component of current has a time constant of 0.15 second. However, as this 0.15 second time constant is larger than the values usually encountered in any equipment except generators and reactors, results are often conservative. In the general case, actual total rms currents are no greater than the "simplified method" values indicated in Figure 1. The d-c time constant may be expressed generally as equal to $(1/2\pi f)(X/R)$ in seconds, or $(1/2\pi)(X/R)$ in cycles, where X is the reactance in ohms at system frequency f and R the d-c resistance to the point of fault.

In this method of determining fault currents, symbols are used with the significance given in the following:

E = phase to neutral voltage
X_1 = direct-axis positive-sequence reactance, either transient or subtransient as specified, in ohms per phase
X_2 = negative-sequence reactance, ohms per phase
X_0 = zero-sequence reactance, ohms per phase
R_0 = zero-sequence resistance, ohms per phase

The type of fault (number of phases involved, with or without ground) which will result in the maximum fault current varies with the relative values of the different circuit constants.

The 3-phase short-circuit current is E/X_1. The line-to-line short-circuit current is $\sqrt{3}E/(X_1+X_2)$. Since X_2 is usually equal to X_1, this current seldom exceeds 86 per cent of the 3-phase short-circuit current, and, consequently, for line-to-line faults the 3-phase fault-current value is generally satisfactory.

The line-to-ground short-circuit current is $3E/(X_1+X_2+X_0)$. Since X_2 is usually approximately equal to X_1 (using the subtransient value for X_1), this expression often is shortened to $3E/(2X_1+X_0)$. In practical cases, regardless of the value X_2, this last expression is satisfactory for determining the conductor current in either single or double line-to-ground fault.

Procedure

The following gives a resumé of the procedure and the various multiplying factors to be used with currents calculated by the formulas just stated. Tables I and II show what reactance quantity should be used for representing the machines in the positive-sequence network.

Interrupting Duty. Determine "highest value of rms symmetrical current for any type of fault" by E/X_1 or $3E/(2X_1+X_0)$, whichever is greater, except that with resistance grounded systems when R_1 is greater than $2.23X_1$, no consideration need be given to the latter expression $3E/(2X_1+X_0)$. Use multiplying factors as given in Table I for specific type of apparatus. It has been

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The original report sponsored in 1942 by the AIEE committee on protective devices was prepared by a working group consisting of W. F. Skeats, W. M. Hanna, H. A. Travers, C. F. Wagner, and C. A. Woodrow. It was revised in 1943 by protective devices committee.

This revision of the report was prepared during 1947–48 by a working group of the AIEE switchgear committee—J. R. North, *Chairman;* O. Bauhan, R. M. Ferrill, S. B. Griscom, W. M. Hanna, R. T. Henry, H. A. Travers, C. A. Woodrow, and D. G. Worth.

Table I. Multiplying Factors for Application of Interrupting Devices

	Multiplying Factor	Reactance Quantity For Use in X_1: Synchronous Generators and Condensers	Synchronous Motors	Induction Machines
A. Circuit Breaker Interrupting Duty				
1. General case				
8-cycle or slower circuit breakers*	1.0	subtransient**	transient	neglect
5-cycle circuit breaker	1.1			
3-cycle circuit breaker	1.2			
2-cycle circuit breaker	1.4			
2. Special case for circuit breakers at generator voltage only. For short-circuit calculations of more than 500,000 kva (before the application of any multiplying factor) fed predominantly direct from generators, or through current-limiting reactors only.				
8-cycle or slower circuit breakers*	1.1	subtransient**	transient	neglect
5-cycle circuit breakers	1.2			
3-cycle circuit breakers	1.3			
2-cycle circuit breakers	1.5			
3. Air circuit breakers rated 600 volts and less	1.25	subtransient	subtransient	subtransient
B. Mechanical Stresses and Momentary Duty of Circuit Breakers				
1. General case	1.6	subtransient	subtransient	subtransient
2. At 5,000 volts and below, unless current is fed predominantly by directly connected synchronous machines or through reactors	1.5			
C. Protector Tubes				
1. Maximum rating	1.0	subtransient	subtransient	subtransient
2. Minimum rating—use minimum system capacity and minimum of E/X_1 or $3E/(2X_1+X_0)$	1.0			
D. Fuses				
1. At 15,000 volts, or below, except for current-limiting fuses when the fuse is located remote from generating stations or primary substations (that is, X/R is less than 4)	1.2	subtransient	subtransient	subtransient
2. All other cases, including all current-limiting fuses regardless of voltage	1.6			

*Since old circuit breakers are slower than modern ones, it might be expected a low multiplier could be used with old circuit breakers. However, modern circuit breakers are likely to be more effective than their slower predecessors, and, therefore, the application procedure with the older circuit breakers should be more conservative than with modern circuit breakers. Also, there is no assurance that a short circuit will not change its character and initiate a higher current flow through a circuit breaker while it is opening. Consequently, the factors to be used with older and slower circuit breakers may well be the same as for modern 8-cycle circuit breakers.

**This is based on the condition that any hydroelectric generators involved have amortisseur windings. For hydroelectric generators without amortisseur windings, a value of 75 per cent of the transient reactance should be used for this calculation rather than the subtransient value.

Table II. Multiplying Factors for Preliminary Relay Settings

	Multiplying Factor	Reactance Quantity For Use in X_1: Synchronous Generators and Condensers	Synchronous Motors	Induction Machines
1. High-speed current actuated	1.0	subtransient	subtransient	subtransient
2. Time overcurrent	1.0	transient	transient	neglect

found that for systems where the 3-phase fault kilovolt-ampere at the generator bus voltage does not exceed 500,000 (before the application of multiplying factors), no additional multiplying factor is required. When calculated values exceed 500,000 kva these multiplying factors should be increased as indicated in Table I, section A2, to compensate for the longer d-c time constant of large generators and reactors.

For the application of protector tubes,[3] both minimum and maximum fault currents must be determined. The minimum fault current should be determined on the basis of minimum connected synchronous capacity and symmetrical current using E/X_1 or $3E/(2X_1+X_0)$, whichever gives the lower value. Special consideration is required where ground resistance appreciably can reduce this line-to-ground fault current. Protector tubes are rated in terms of symmetrical current but are designed to withstand full asymmetrical current and therefore the multiplying factor is 1.0.

Fuses are rated on total current, and, as the time durations involved are very short, their required interrupting capacity should be based on the total rms current at one-half cycle, using subtransient reactances for both synchronous and induction machines. Two cases should be considered.

1. At 15,000 volts or below, except for current-limiting fuses, when the fuse is located remote from generating stations or primary substations (that is, X/R is less than 4) multiply by 1.2. This is because the rms current of a maximum displaced wave in the first half cycle will not exceed 1.2 times the calculated symmetrical current.

2. In all other cases, including current-limiting fuses, regardless of voltage, multiply by 1.6.

In determining the X/R ratio in the calculation, the entire X value from source of power to the fuse location should be used but the R value only from the fuse back to the first primary substation. The error in neglecting the remainder of R is negligible, and the difficulty in obtaining rigorous values is not commensurate with the increase in accuracy obtained. If the X/R ratio is less than 4, the lower multiplying factor of 1.2 is permissible; otherwise the factor of 1.6 should be used. Although it is recognized that the same line of reasoning could be applied to higher-voltage fuses which are not current limiting, a survey of many cases indicated that the majority of these cases would fall in the 1.6 multiplier class.

For all current-limiting fuses, the multiplying factor will be 1.6. This is because the rating of such fuses is based on an unsymmetrical current value equal to 1.6 times the symmetrical current.

Mechanical and Momentary Duty. For many purposes, it is necessary to know the maximum possible rms current (including both a-c and d-c components) which can flow in the circuit. Allowing for over-excitation of generators on account of load, the rms value of current as calculated for zero time is about 1.8 times the value calculated by dividing phase to neutral voltage by subtransient reactance. As there will always be some delay even in the first half cycle; however, a multiplier of 1.6 is acceptable.

In some instances, as when transmission line or cable reactance is an important factor in the limitation of current, the factor 1.6 may be higher than necessary. In such applications at 5,000 volts and below, the ratio of reactance to resistance is sufficiently low so that the d-c component decreases very greatly during the first half cycle. Thus within this voltage range, and except where the bulk of the power is supplied by generators at the point of fault, a factor of 1.5 may be used instead of 1.6.

Low-Voltage Systems. Low-voltage air circuit breakers (rated 600 volts or less) are often instantaneous in operation and part contacts during the first half cycle. These circuit breakers, however, are rated on the basis of average current in

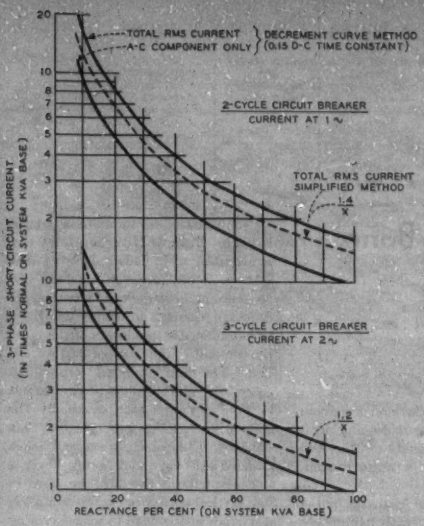

Figure 1A

——— Decrement curve method
——— Simplified method

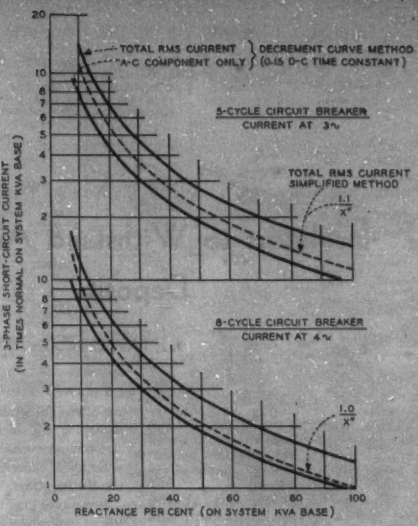

Figure 1B

——— Decrement curve method
——— Simplified curve

the three phases, and circuits on which they are installed rarely have X/R ratios exceeding 12. This corresponds to an average rms current at one-half cycle after instant of fault which equals 1.25 times the symmetrical current. Such circuit breakers therefore may be applied on the basis of 1.25 times the 3-phase initial symmetrical current using subtransient reactance and including both synchronous and induction motors.

In calculating the equivalent system impedances, it should be remembered that at low voltages even small impedance values become of importance, and all elements of the circuit, including current transformers, disconnects, switches, bus runs, and their connections should be taken into consideration.

Overcurrent Protective Relays. In approximating the settings of overcurrent relays, the fault currents for two conditions should be determined:

1. The maximum initial symmetrical current for maximum connected synchronous capacity as determined by E/X_1 or $3E/(2X_1+X_0)$, whichever is greater, except that, when R_0 is greater than $2.23X_1$, no consideration need be given to the expression $3E/(2X_1+X_0)$.

2. The minimum symmetrical current for minimum connected synchronous capacity as determined by $0.866E/X_1$, or $3E/(2X_1+X_0)$ for reactance grounded systems. In particular situations, allowance should be made for remote fault locations and fault resistance.

Ground, distance, balanced, and other types of relays require special consideration.

References

1. System Short-Circuit Currents, W. M. Hanna, H. A. Travers, C. F. Wagner, C. A. Woodrow, W. F. Skeats. AIEE Transactions, volume 60, 1941, September section, pages 877–81.

2. Simplified Calculation of Fault Currents. Electrical Engineering, October, 1942, pages 509–11; and AIEE Transactions, 1942, pages 1133–35; Electrical Engineering, February 1944, page 65.

3. Protector Tubes for Power Systems, H. A. Peterson, W. J. Rudge, Jr., A. C. Monteith, L. R. Ludwig. AIEE Transactions, volume 59, 1940, May section, pages 282–92.

4. Standard Decrement Curves, W. C. Hahn, C. F. Wagner. AIEE Transactions, volume 51, 1932, pages 353–61.

No Discussion

Insulators to Withstand Air-Borne Deposits

J. J. TAYLOR
ASSOCIATE AIEE

Synopsis: Representative types of fog insulators are classified according to design principles. The general conclusion is that each type has inherent limitations and that a completely satisfactory solution of the problem is still to be found.

The mechanism of leakage surge flashover is examined. Continued research into methods of establishing a voltage gradient by resistance means is recommended. In such research, leakage surge counters promise to be a useful tool. A description is given of surge measuring equipment and of results obtained on the group of outdoor tests.

HIGH-VOLTAGE insulators are designed to serve as mechanical supports and electrical barriers. That they may fail in their electrical function because of excessive surface dirt and moisture was recognized at least 70 years ago. In 1878 Johnson and Philips patented the oil-filled insulator shown in Figure 1, Group 1(A). A few years later Alf Sinding-Larsen of Norway invented an insulator with heating elements to maintain a dry surface. His design is illustrated in Figure 1, Group 7(A).

The distribution of voltage over an individual insulator, insulator string or stack, under clean laboratory conditions, is determined by the inherent electrostatic capacity of the insulating shells or assemblies. In many applications of insulators this capacitance 'grading' is almost completely overruled by surface leakage through a conducting film of dirt and moisture.

An obvious alternative to capacitance grading is the use of resistance elements to control the division of voltage over the individual parts of a single unit or the various sections of a multiple assembly. Capable of contributing to such an approach, there is today a rapidly growing interest in semiconductors; a vigorous technology in silicon carbides and negative-temperature-coefficient resistors and a decade of experience in the use of semiconducting glazes.

Resistance grading of insulators by the use of semiconducting glazes has already shown considerable promise both in laboratory tests and in limited service applications. It is, at any rate, a relatively new science, largely unexplored. After 70 years of inquiry and experiment with capacitance graded units it may be assumed that this art has been liberally explored and that a more promising avenue should be sought for future research. In Figure 1 an attempt has been made to choose from many hundreds of insulator designs a few that represent diverse principles and to classify these according to type.

Classification of Fog Insulators

Oil-filled insulators illustrated in Group 1, Figure 1, do not promise a satisfactory solution of the surface leakage problem. Although the oil zone may be initially effective, it is soon contaminated by moisture and salts. Oil tends to creep over external insulator surfaces and to encourage collection of dust. Handling of the insulator is difficult and the angle of string inclination must be kept low to prevent loss of insulating fluid.

In insulators of this general type, designed to provide a protected area that repels moisture, the use of certain silicones or other surface treatments would be a more modern approach. Apparently there has not yet been developed a surface condition or treatment that will inhibit the deposition of dirt and moisture over a period of years.

Group 2 includes a standard suspension insulator. Because of its ruggedness, ease of manufacture and adaptability to close spacing, it has given an excellent performance in the majority of service applications. Attempts to modify this shape by extending the petticoats or the outer rim emphasize limitations of the standard disk. The deep sheltered corrugations collect dirt which cannot be cleaned out by wind or rain so that, eventually, the units may become less effective than standard insulators. In general, such modified units have a low wet flashover, a tendency towards flooding when used in long vertical strings and a very considerable handicap in regard to manufacturing cost.

Smooth disk units shown in Group 3 sacrifice both impulse and power frequency flashover strength in order to afford easier cleaning. In localities where cleaning by wind and rain is inadequate these insulators, when wetted, display vigorous corona around the pin and cap, particularly around the pin because of the absence of petticoats normal to the flange which limit streamer formation in standard disks. Such overstress near the terminals maintains dry areas and helps to prevent power frequency arcover but causes radio interference.

The insulators shown in Group 4 have been popular experimental types in areas where there is a large amount of rainfall and where the contaminant is a combination of salt, fog, and industrial dirt. The length of leakage path is not sufficient to hold leakage current at a reasonable level with all surfaces wet and contaminated.

Insulators in Group 5 combine features of Groups 2 and 4. They have the length of external surface greatly increased to limit leakage current; sheds are exposed to improve cleaning by natural agents. A considerable portion of sheltered surface is provided in which annular petticoating

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J. J. TAYLOR is chief engineer with the Ohio Brass Company, Barberton, Ohio.

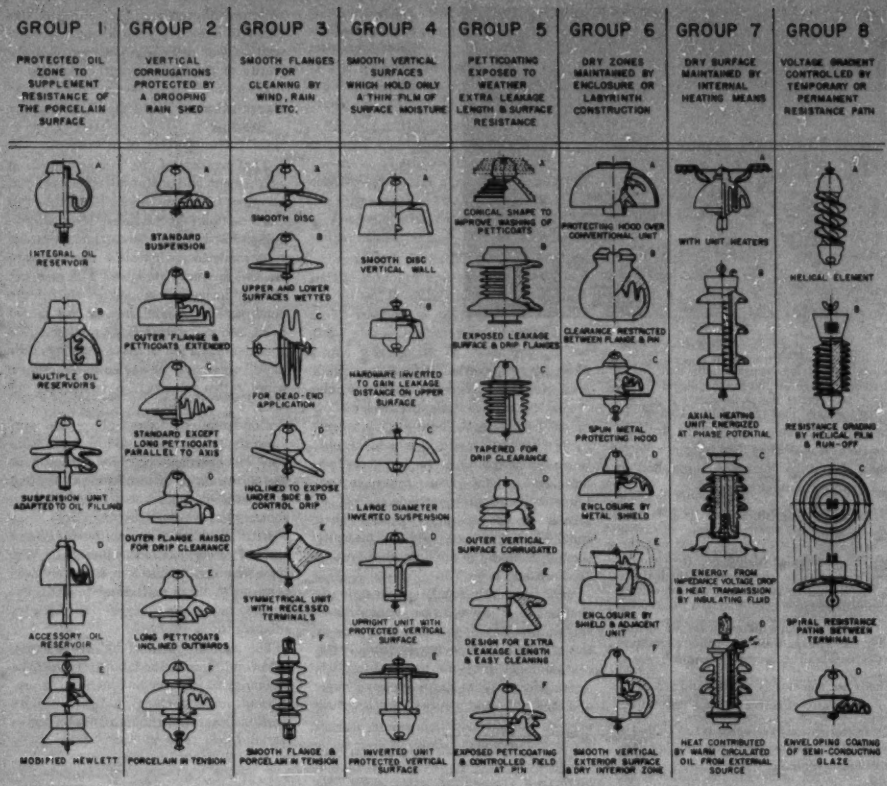


Figure 1. Classification of fog insulators by design principle

may be used to reduce radial discharges from the insulator pin. Some designs from this group have given useful service in both coastal and industrial areas.

Insulators in which primary reliance is placed on dry zones maintained by fairly complete enclosure are shown in Group 6. Carried sufficiently far, this approach could theoretically guarantee dry areas great enough to hold a working voltage with neither electrical overstress nor excessive leakage current; actually, limitations of cost and space make it impractical to carry this principle to a fully satisfactory conclusion.

An attempt may also be made to maintain dry zones by artificial heating, with or without a decided measure of enclosure, as illustrated in Group 7. Complexity of design, power loss, and fragility are obvious limitations in this method of attacking the problem.

In Group 8 insulators, distribution of voltage is controlled by conducting, high resistance paths disposed in ribbon form or as an enveloping layer over the dielectric surface. They may be either temporary or permanent in nature. An attractive feature of temporary resistance paths (established only during adverse weather) is that a continuous power loss is avoided. Moreover, an insulator surface carrying leakage current in a water film is not likely, under any condition, to reach a temperature in excess of 200 degrees Fahrenheit. This desirable limitation does not necessarily exist when permanent resistors are built in or adjacent to the insulator surface.

Since conducting surfaces appear to be unavoidable in many types of service, the idea of making a merit of this necessity warrants serious consideration.

Recommendations for Deposit Conditions

A number of investigations of insulator performance under adverse operating conditions have been made by competent and diligent workers. In general, these investigators are reluctant to ascribe outstanding merit to a particular design. The following conclusions are typical:

"Early in the investigations described in this paper, it was realized that development of an insulator that would be the answer to the dust and fog problem would be possible in a short period of time."[4]

"There is no virtue in any particular shape except as it provides surface leakage resistance."[4]

"A surface leakage resistance of 11.0 inch-

Figure 2. Overstress on contaminated insulators

units per string appears sufficient for a steady voltage of 150-kv to ground."[1]

In terms of leakage distance this would be about 1.5 inches of leakage distance for each kilovolt of applied voltage, a figure that should be increased somewhat to take care of voltage surges.

From an English paper on insulator performance:[3]

"For use under highly polluted atmospheric conditions, the length of the leakage path—should be two inches per kilovolt of applied voltage."

"It is necessary in addition to ensure that the leakage path has other characteristics besides the necessary length."

Sheds not too closely spaced and arranged to contribute effective surface resistance.

"Investigations are being made to find a satisfactory method of incorporating stabilizing resistance within the units. When this has been achieved, the resulting insulator should have a very satisfactory performance in polluted atmospheres."

A distinction has been made between designs intended for industrial areas and those intended for sea coast deposits. For example:

"For industrial deposits a design should afford a maximum amount of upper surface which is washable by both wind and rain."[5]

"For salt deposits a design should supply a maximum amount of protected surface."[5]

After a desirable design has been chosen it may prove necessary to establish a periodical schedule of insulator washing. Though the preferred design must be washed eventually, it may have considerable merit if it can pass successfully, without washing, through one or more complete "fog" seasons.

Over-insulation has been proved a successful remedy in some areas, particularly where heavy industry is the source of contaminant. In this connection it is interesting to note that if large and small insulators have a similar geometry, the theoretical surface resistance does not increase with size. There are, however, many types of service in which the uniform film assumed in surface resistance formulae is not even approximated. Large units, though perhaps subject to greater maximum surge currents than small units, are better able to dissipate them and have the advantage of greater arc clearance.

Very frequently insulation on a line is changed to improve its performance and this change may be followed by a year or two of optimistic reports. Eventually the new units get dirty and the final result is disappointing. Since our increasing levels of industrial concentration are arousing widespread interest in the subject of this paper it is well to keep in mind the desirability of deferring judgment on the merits of insulators recently installed.

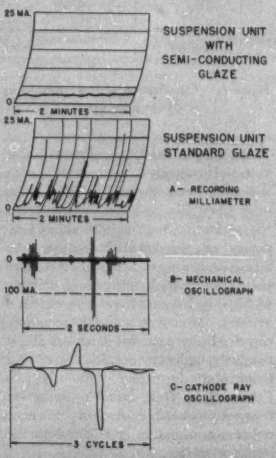


Figure 3. Leakage currents recorded on units with and without semiconducting glaze

Correlation of Field and Laboratory Studies

That weather of one sort or another is ever present and of almost endless variety, accounts for its popularity as a subject of conversation. It explains, also, a difficulty that is fundamental in laboratory tests made to evaluate the merit of contrasting insulator designs. The prospect of reproducing in a laboratory the infinite combinations of contaminant, air movement and precipitation is appalling. To consider only a few of these, fog may occur in still air or may be driven in by onshore winds with a velocity of 20 to 30 miles per hour.

Condensation takes place on surfaces sheltered against rain or fog. Rain may be beneficial or very harmful to the performance of insulators, depending on whether it appears as a quick vigorous shower that cleans exposed surfaces, or as a drifting drizzle that wets both exposed and hidden surfaces of the dielectric. Wet snow may pack inches deep in the crevices between shells or units and produce extremely high leakage surges over dirty insulators.

The impracticability of reproducing field conditions in the laboratory and the ingenuity of weather in contriving a previously unheard of condition suggests caution in recommending insulator types

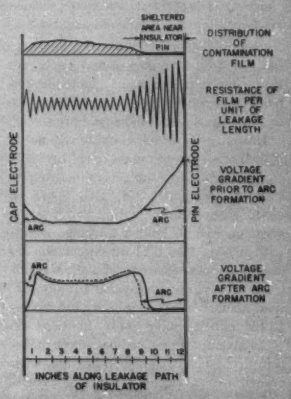


Figure 4. Leakage surge mechanism, insulator with normal glaze and surface contamination

The film resistance is shown symbolically

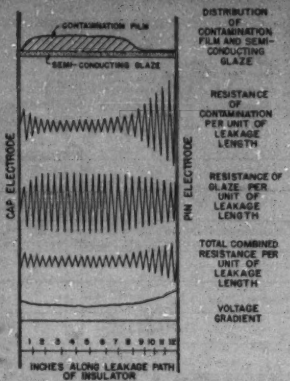


Figure 5. Insulator with semiconducting glaze to improve voltage gradient

that might be vulnerable not only to flashover and a temporary outage, but to partial or complete failure. Certainly a great deal of experimental testing under actual service conditions should precede the recommendation of insulators employing permanent resistance grading, no matter how promising laboratory tests may appear.

Mechanism of Leakage Surge Flashover

Casual field observation shows that the voltage division over the several shells in an insulator stack or over the different units in a suspension string is by no means uniform. Due largely to the workings of chance, a few areas or units carry a large proportion of the voltage. This is illustrated in Figure 2. Insulator units that are arcing vigorously have high over-all surface resistance and carry an undue share of voltage except during that instant when a leakage surge takes place. In the intervals between surges inactive units support much less than their share of line voltage.

"Fog-box" tests of units with and without semiconducting glaze are shown in Figure 3. Both insulators were equally contaminated and were tested at 10 kv. No surges occurred on the treated insulator; it had a steady leakage current of 2.5 milliamperes.

Frequent leakage surges were recorded on the standard unit as shown in Figure 3A, B, and C. The surge time was only a small fraction of total elapsed time so that, although the upper limit of surge peaks was about 75 milliamperes, the average effective leakage current was comparable to that observed on the unit with semiconducting glaze.

A surge condition can exist for many hours and presents, therefore, an uneasy but recognizable state of equilibrium: the energy loss represented by the area under the leakage current curve, in combination with the amount of dirt on the insulator and the rate of moisture addition, contrives to maintain an adequate average level of surface resistance, and permits voltage to be carried without complete flashover of the string. Inactive units in the string that are dark and silent and coated with a continuous film of electrolyte develop only a small voltage drop while the leakage current lies near the base line but are effective in reducing voltage across active insulators as each surge peak develops. They may perform a useful function in preventing flashover, even though they do not carry their share of voltage during a large portion of the total time.

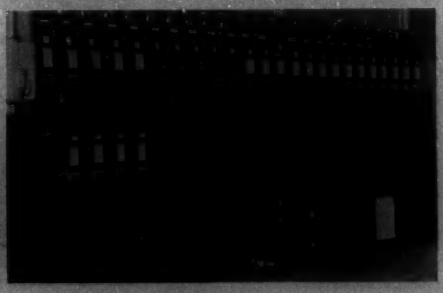
Instability of the moisture film on the insulator surface explains the recurrent surging that is observed. This is illustrated in Figure 4. Wet and dirty insulators are not uniformly conducting over the leakage surface. When one area of the insulator develops a thinner, high resistance film, a predominant amount of voltage appears across this drier area. If the voltage that appears is sufficiently great an arc short-circuits what was formerly a voltage-carrying surface and the ampere level reached is then limited largely by resistance of the remaining unshorted film. There is an extremely high momentary watt loss that burns back and thins out the film. This increased resistance quenches out the arc and reduces the leakage current to its lower envelope. An increasingly bad condition on the insulator string may be recognized by either a closer time spacing of the temporary surges or by surges equally spaced in time but of greater duration or magnitude.

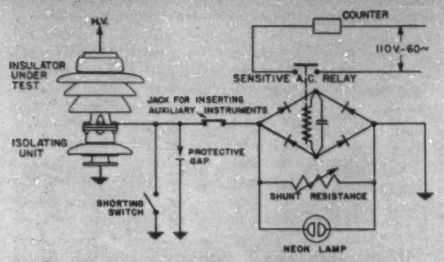
Laboratory tests indicate that flashover of a string or stack is not likely to occur until surges reach a crest value in the order of 100 or 150 milliamperes. Under the majority of conditions, average leakage currents are far below this level and a possible solution of the problem is, therefore, to arrange a method of cutting down the high intermittent surges at the expense, if necessary, of raising the lower envelope of leakage current and the energy loss over insulation in fair weather. This result can be accomplished by building a slightly conducting film in the insulator surface separate from the deposited film of dirt and moisture and serving to level out the abrupt discontinuities that appear in the surface film. Figure 5 illustrates, in a qualitative manner, the function of a semiconducting glaze film. The height of the resistance symbol represents areas of high and low

Figure 6. Test rack continuously energized at 44 kv

Figure 7. Surge-counting panel

The self-contained instruments (shown lower left) are for use on utility properties and are set to count surges at 20, 50, and 100 milliampere levels





LEAKAGE CURRENT OVER INSULATOR PASSES THROUGH SENSITIVE A.C. RELAY. OPERATING CURRENT OF RELAY IS SET BY SHUNT RESISTANCE. EACH OPERATION OF A.C. RELAY REGISTERS ONE COUNT ON 110 VOLT COUNTER. NEON LAMP PROTECTS RELAY FROM OVERLOAD CURRENTS. SEVERAL IDENTICAL CIRCUITS CAN BE CONNECTED IN SERIES WITH RELAYS SET AT DIFFERENT OPERATING CURRENTS IN ORDER TO CLASSIFY SURGES ACCORDING TO AMPLITUDE.

Figure 8 (above). Leakage current surge counting circuit

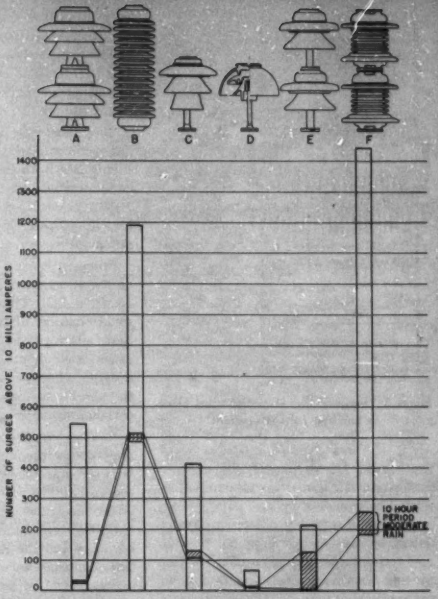


Figure 9. Leakage surge counts obtained over a 14-month period on insulators energized at 44 kv

resistance in the parallel films of contaminant and glaze.

Semiconducting glazes are in general use to distribute voltage stresses adjacent to insulator terminals. This original function has been extended to effect potential distribution over the whole insulator surface and has shown promising results in laboratory and field trials. It has been a subject of extensive research in Great Britain and several informative references are given at the end of this paper.[2,3,9]

Semiconducting Glazes

The use of semiconducting glazes, extending from terminal to terminal of an insulator is not a particularly new idea. Several patent applications are on record, applied for in the decade 1920 to 1930. A considerable time elapsed between conception of the idea and practical development of such glazes. Successful employment of semiconductors argues a level of process control not yet easily attained in the technical porcelain industry. Such glazes are influenced profoundly by minor impurities, small variations in kiln atmosphere and variations in composition of the clay body to which they are applied. To be commercially acceptable, a semiconducting glaze should have the following qualifications:

1. Approximate the hardness, chemical resistance, and strength of a normal glaze.
2. Permit the use of existing manufacturing equipment.
3. Yield a high percentage of acceptable product.
4. Show long term stability under voltage.
5. Carry much greater than normal currents for short intervals of time without excessive heating or burn-out.
6. Resistant spalling from the sudden cooling of hot surfaces.
7. Allow electrical tests to establish the dielectric strength of components and assemblies.

Ordinarily, semiconductors have a decided negative temperature coefficient, a mixed blessing which contributes to their value in stabilizing surface films of dirt and moisture, but renders them liable to self-destruction in their effort to effect compensation during prolonged and unexpectedly severe conditions of service. In order to anticipate such severe conditions of service, there is an urgent need of accurate information about the nature and magnitude of energy loss over insulators now in use in sea-coast, railway, and heavily industrialized areas. If this information can be acquired for the insulator designer, and if he may also choose from a wide variety of conducting glazes, he should be able to contrive insulator contours peculiarly suitable to resistance glaze application and particularly fitted for a known type of service.

To supplement laboratory tests on artificially contaminated insulators, a permanent outdoor rack (Figure 6) has been set up and a variety of insulators energized at 44 kv. Each string is connected to ground through a surge counter circuit (Figures 7 and 8) which permits the insertion of auxiliary instruments.

Figure 9, a 14-month record of surge counts, illustrates the unpredictable nature of leakage current surges. A single period of moderate rain contributed 60 per cent of the surges recorded against type "E" yet only a very modest percentage to types "A" and "C" which are similar in design.

Conclusions

1. Wet and dirty insulators distribute voltage in proportion to surface resistance.
2. Of insulator types employing capacitance 'grading,' for exposure to sea coast or industrial contamination, some of the designs from Group 5, Figure 1, present a useful combination of leakage distance, drip-clearance, ruggedness, and self-cleaning.
3. Inbuilt capacitances are capable of controlling completely only mild exposures to dirt and moisture.
4. Inbuilt resistance promises a more powerful control at the expense of fair-weather power loss and some increase in the hazard of insulator damage.
5. Specific information is needed about the nature and magnitude of energy loss over various styles of insulators in a variety of service conditions. Surge measuring equipment is being installed on utility properties for this purpose.
6. Semiconducting glazes should be developed and their successful application

promoted by an increasing measure of raw material and process control.

7. Continued co-operation of insulator users to determine both the risks and possibilities offered by this avenue of development can contribute greatly to advancement in the art.

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Discussion

Bradley Cozzens (Department of Water and Power, Los Angeles, Calif.): The paper as presented by Taylor gives a brief and concise history of the mechanism of breakdown of contaminated insulator surfaces, and points out the extended research and development that has been conducted over many years on shapes and methods of supporting insulators to relieve the difficulties occasioned by contamination.

By way of comment, the insulator shown as item (B) of Group 6 in Figure 1 of Taylor's paper, which was developed largely in response to the need for an insulator which would prevent pin burning in a seacoast area, has proved relatively satisfactory and has accomplished the end for which it was designed. The Department of Water and Power, Los Angeles, Calif., has considerable 4,800-volt ungrounded delta feeders operating adjacent to the seacoast with heavy salt contamination. This insulator, with a slight modification, carrying a drip ring at the midsection, is used almost exclusively in such areas. The insulator shown as item (E) of Group 6, is approximately the design which was suggested as a result of the test procedures reported in another paper.[1] The close coupling of this labyrinth makes it very difficult of assembly, and once contamination would occur it would be practically impossible to clean such an assembly without complete dismantling. However, there is definite precipitator action which should keep the inner portions of this insulator clean over an extended period of time, as well as preventing the accumulation of moisture within the area adjacent to the cap and pin.

Referring to Figure 4 of the paper, I have some question as to the area marked "sheltered area near insulator pin." If I interprete the curve properly, this would indicate that little or no contamination occurs in this area and, therefore, the resistance of this area is particularly high. Studies conducted in connection with dust depositing, as shown in Figure 4 of the paper by Cozzens and Blakeslee,[1] with particular reference to curves 3 and 4, indicate that the accumulation of dirt or dust on and adjacent to the pin, and immediately adjacent to the cap except for the area shielded by the cap bead, is appreciably greater than that over the remaining surface of the insulator. Checks of voltage distribution over the insulator surface, curve 3 of Figure 4, shows clearly that the contamination is appreciably greater adjacent to the pin than in other areas. I believe that the results as indicated in Taylor's paper probably were due to the fact that dust was not deposited in sufficiently high velocities, or possibly was deposited while the insulators were not energized, either of which will cause the condition as shown.

In the author's Conclusion (5), he states that "Specific information is needed about the nature and magnitude of energy loss over various styles of insulators in a variety of service conditions. Surge measuring equipment is being installed on utility properties for this purpose." It was found during the laboratory tests on insulators that the surge periods of high current, though for the period of time which they existed contributed appreciably to the heating of the insulators, were only partially responsible for the total heating which occurred. Leakage current of the order of two to five milliamperes may exist continually and, except in very heavy fog, with appreciable movement so that moisture is driven against the face of the insulator, the surges of highest current occur at relatively infrequent intervals.

On the initial application of fog after an accumulation of dust, if such application is quite sudden, the leakage currents are very high, developing first of a continuous nature and as units dry in the high current density area becoming more of the nature as indicated in Figure 2 of paper by Cozzens and Blakeslee.[1]

It appears that in order to get a proper answer to the energy loss some watt integrating device would be necessary. The recording of surges as could be seen from Figure 2 would be of little value since many of these surges exist for only one or two cycles, while others exist for as much as 60 or more cycles. The continuous current which exists between the high current surges also must be included in the determination of over-all heating of the insulator surface. The tests that have been conducted by the Department of Water and Power thus far have not attempted to determine accurately the power loss for current values below three milliamperes. This current flow is relatively continuous. In the range above this minimum current value, the intermittent arcing contributes a relatively small amount of energy loss. Determinations of some ten tests showed a variation in average loss from 18 to 175 watts. This, of course, was superimposed upon the loss resulting from the continuous low current leakage.

Reference

1. See reference 4 of the paper.

J. J. Taylor: The author wishes to thank Cozzens for his interesting discussion.

Insulators of both types *6-E* and *6-F* have had periods of trial installation on west coast lines. Eventually sufficient dirt was deposited on interior surfaces to make operation of the insulators audibly noisy. Condensation in the interior, on the dirt film, resulted in low amperage internal discharges and the hollow shapes amplified the noise.

Regarding the sheltered area shown in Figure 4 of the paper, our inquiry confirms Cozzens' observation that dirt deposit is heaviest near the pin; the high resistance indicated for this surface is the net result of moisture plus contamination and controlled largely by the thinness of the moisture film.

From surge measuring equipment we do not expect to get a direct measurement of energy loss but rather a series of observations that, by correlation with similar records made in the laboratory and with more delicate equipment, may permit estimates of contamination severity.

A New Distance Ground Relay

S. L. GOLDSBOROUGH
MEMBER AIEE

DISTANCE-type relays have been used successfully for many years to protect against phase faults on transmission lines. However, distance relays to protect against ground faults in the same satisfactory manner as for phase faults have not been generally available. Some attempts have been made to adapt distance phase relays for operation on ground faults. In a small number of cases the results have been reasonably satisfactory but, in general, the fundamental limitations pertaining to these relays have prevented their widespread application. The new relay discussed here is an attempt to satisfy the long felt desire for a satisfactory distance ground relay.

It should be interesting to briefly review some of the factors involved in the design of a distance ground relay. Since a phase distance relay uses delta line voltage and delta line current, it no doubt occurs to many engineers that a ground distance relay would operate on line-to-ground voltage and line current. This, however, is not a valid conclusion because ground faults introduce a factor not existing on phase faults which must be considered. The additional element to be considered is the fact that, in general, the zero sequence impedance of a transmission line is not equal to the positive-sequence impedance. This means that, as the distribution of the positive, negative, and zero sequence currents varies because of changes in system connections outside the protected line section, the ratio of line-to-ground voltage to line current changes thus causing a variation in the ohm reach of the relay. This can be seen more readily if we express the quantity which the relay would be required to measure in symmetrical components. Referring to Figure 1 (a) the impedance at the relay location R is:

$$\frac{I_1Z_1+I_2Z_2+I_0Z_0}{I_1+I_2+I_0} \tag{1}$$

assuming Z_0 to be $3Z_1$ it is easily seen from this that, as the relative values of the positive, negative, and zero sequence currents are changed in a certain fashion the corresponding voltage drops do not change in the same manner. For instance, if we change the external system connections as shown in Figure 1 (b) where all the positive and negative currents come from the generator through the relay and all of the zero-sequence current comes from the right-hand grounded transformer bank, it can be seen that the relay current magnitude is reduced to two thirds, whereas the voltage is reduced to two fifths of the former value. Thus the line to ground voltage to line current ratio has been drastically altered by system changes external to the line section. On the other hand, a phase relay responds to the ratio

$$\frac{I_1Z_1+I_2Z_2}{I_1+I_2}$$

and here it is seen that external system connections cannot affect this ratio. The positive-sequence impedance of the line section is always equal to the negative sequence and, in general the positive and negative sequence fault currents are nearly equal. However, if the relative values of I_1 and I_2 change the relative values of I_1Z_1 and I_2Z_2 will change in exactly the same manner so that the voltage to current ratio is not changed. This same situation would apply on ground faults if the zero-sequence impedance were equal to the positive-sequence impedance. However, the zero sequence is nearly always considerably higher than the positive-sequence impedance and this has the effect of adding an extra voltage drop during ground faults over and above the phase fault value which is not compensated by an additional current component. If Z_0 were equal to Z_1, the impedance on a single-phase-to-ground fault would be

$$\frac{I_1Z_1+I_2Z_1+I_0Z_0}{I_1+I_2+I_0}=\frac{3I_1Z_1}{3I_1}=Z_1 \tag{2}$$

but, assuming $Z_0 = 3\,Z_1$ the expression is

$$\frac{I_1Z_1+I_2Z_1+I_0Z_1+2I_0Z_1}{I_1+I_2+I_0}=\frac{5Z_1}{3} \tag{3}$$

Thus the additional voltage drop $2\,I_0Z_1$ has been created solely because Z_0 is 3 times Z_1 instead of equal to Z_1 but no additional compensating current has appeared so the indicated impedance is different. Inspection of the above expression indicates that if the current $2\,I_0$ be added to the denominator the expression would reduce to Z_1. Further consideration will reveal that for the general case an additional current I_0 modified by a factor $\frac{Z_0-Z_1}{Z_1}$ must be added to the line current operating the relay in order to compensate for the additional voltage drop caused by that part of the zero sequence line impedance which is in excess of the positive sequence impedance. The general expression for the relay on the grounded phase and neglecting fault resistance is, therefore,

$$\frac{E_{LG}}{I_{line}+\frac{(Z_0-Z_1)}{Z_1}I_0}. \tag{4}$$

A distance relay which utilizes the above quantities is essentially a phase re-

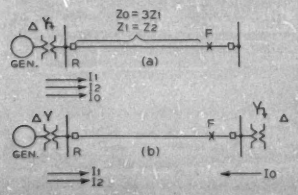

Figure 1. Single line diagram of simple system

Paper 48-260, recommended by the AIEE relay committee and approved by the AIEE technical program committee for presentation at the AIEE Midwest general meeting, Milwaukee, Wis., October 18–22, 1948. Manuscript submitted July 19, 1948; made available for printing August 27, 1948.

S. L. Goldsborough is with the Westinghouse Electric Corporation, Newark, N. J.

lay with a current compensation added to enable it to function properly on ground faults as well as interphase faults. Such a relay has been available for a number of years but its application has been very limited because of certain fundamental characteristics.

It is quite essential that a ground relay respond to reactance rather than impedance, because the amount of fault resistance liable to be encountered on ground faults is much higher than is present on interphase faults, so that an impedance relay ohm reach would be too drastically reduced. The requirement that the relay measure reactance introduces a severe limitation which comes about solely because of the characteristics of a reactance element. It is well known that a reactance element operating on line current will readily trip during normal load conditions, since the apparent reactance value presented to the relay may be a low positive value, a zero value, or even a negative value. For this reason it is necessary to employ an impedance element to supervise the reactance element since the apparent impedance as seen by the impedance element is always positive on faults and all load conditions. Since fault resistance values on ground faults are relatively high, it is necessary to set the supervisory impedance element for a long reach. A long enough reach on the impedance element to encompass the maximum fault resistance often means that the impedance element responds to maximum load current. Since response to load currents is not permissible, it is necessary to shorten up on the impedance element and be content to forego tripping on some high resistance faults. The limitation on load current is not too serious since a satisfactory discrimination between fault current and load current could be obtained except for the longer line sections. However, it turns out that it is necessary to make a discrimination between fault magnitude of current on the unfaulted phases and fault current on the faulted phase. On an actual system wherever it is possible for positive and negative currents to flow from one generating source to a ground source located elsewhere, there will be a flow of fault current on all 3-phase conductors for a single-phase-to-ground fault. This state of affairs is quite common and occurs in varying degrees on nearly all power systems. Figure 1 (b) is an example of this condition, positive and negative sequence currents flowing on all three conductors from the left-hand end and zero sequence currents flowing on the three conductors from the right-hand end. It is obvious that the supervising impedance elements in all three relays will be actuated under these conditions and thus the reactance element in at least one relay on the unfaulted phase will overreach and trip for external faults. If the impedance element's reach is restricted enough to prevent its operation under this condition then on the faulted phase it would not reach far enough to allow the reactance element to cope with a reasonable amount of fault resistance.

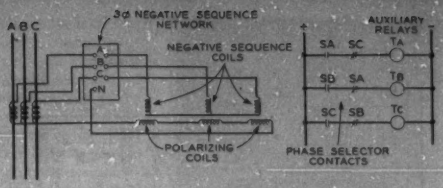

Figure 3. Schematic connections of phase selector elements

The particular ground relay described in reference 2 was beset by another difficulty. As described in reference 1, a line current compensated ground relay being essentially a phase relay will respond on 2-phase-to-ground faults. For instance, on a B-phase to C-phase-to-ground fault both the B- and C-phase relays respond, but each relay will either overtrip or undertrip in opposite fashion depending upon whether the phase-to-phase fault resistance is smaller than the phase-to-ground resistance or vice versa. Since it is impossible to predetermine what state of affairs will exist, it was necessary to block tripping by means of the operation of two or more supervising impedance elements. This, of course, means that all tripping on bona fide single-phase-to-ground faults where fault current occurs on all conductors is blocked. If, for some reason, it had not been necessary to block tripping on 2-phase-to-ground faults one would have merely exchanged the failure to trip at all for the failure to confine tripping to faults inside the line section.

The foregoing discussion has brought out the main difficulties involved in attempting to adapt a phase relay to perform correctly on ground faults. It is to be noted that the trouble is caused by the use of a reactance element operating on line current. It would, therefore, appear that a reactance relay operating on zero sequence current alone would avoid the difficulties mentioned previously. Furthermore, the use of zero sequence provides an automatic fault detector action since current is present only during fault conditions. A supervising impedance element is thereby dispensed with. Also, there is only one zero sequence current, therefore, as far as the current circuit is concerned, only one relay is needed to provide ground protection on all three phases.

Referring to Figure 1 and equation 1, it can be seen that if we eliminate the positive- and negative-sequence currents and also the positive- and negative-sequence voltage drops from the relay to the fault the expression reduces to

$$\frac{I_0 Z_0}{I_0} = Z_0$$

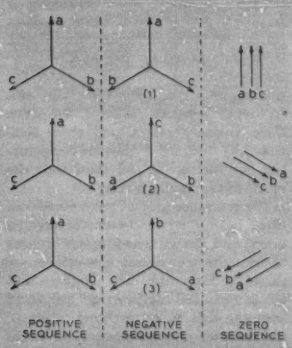

Figure 2. Phase relations of sequence currents on single-phase-to-ground faults

Current vectors for single-phase-to-ground faults on different phases

(1)—Phase-A-to-ground fault
(2)—Phase-B-to-ground fault
(3)—Phase-C-to-ground fault

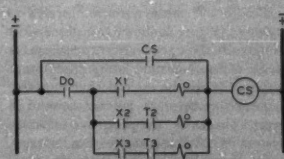

Figure 4. Trip circuit connections of ground reactance relay

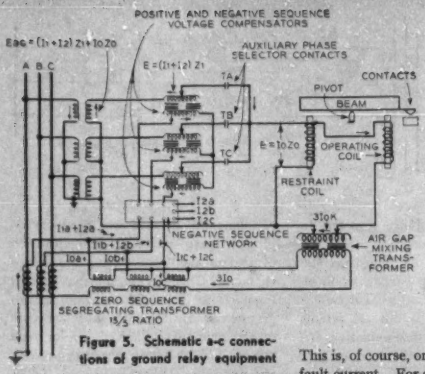

Figure 5. Schematic a-c connections of ground relay equipment

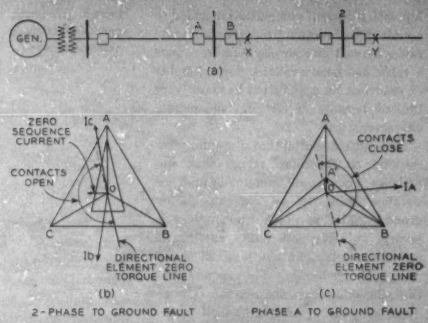

Figure 6. Conditions during 2-phase-to-ground and single-phase-to-ground faults

where Z_0 is the impedance from the relay to the balance point. The positive-and-negative-sequence voltage drops can be eliminated from the line-to-ground voltage by passing the positive-and-negative-sequence secondary currents through a suitable compensator which will develop a voltage representative in magnitude and phase angle of the primary positive-and-negative-sequence voltage drops. This type of compensation is called voltage compensation and is covered in complete mathematical terms elsewhere.[1]

Since there are three line-to-ground voltages, it is necessary to subtract the positive-and-negative-sequence drops in each phase from the corresponding line-to-ground voltage. Therefore, from the standpoint of the voltage circuit, three ground relays or their equivalent are required. However, it appeared to be very desirable to take advantage of the phase selector relay developed for single-pole tripping[2] to switch the proper faulted phase-to-ground voltage into the voltage circuits of a single ground relay. This type of phase selector operates on the phase shift between the negative-sequence and zero-sequence currents depending upon which phase conductor a ground fault occurs. It is very sensitive and has given a good account of itself during several years of actual service.

Briefly the phase selector operates as follows. Figure 2 shows the phase shift among the three sequence components for single-phase-to-ground faults on different phases. It will be noticed that the zero-sequence component rotates around a given phase of the negative-sequence system in 120 degree steps. For instance, on a phase-A-to-ground fault the zero-sequence current is equal to and in phase with phase A of the negative sequence. This is, of course, on the basis of the total fault current. For a fault on phase B the zero-sequence component is in phase with the B-phase negative sequence, and, similarly, for a C-phase-to-ground fault the zero sequence is in phase with the C-phase component of the negative sequence. Therefore, a selector element utilizing zero- and negative-sequence current will act in one direction for phase-A-to-ground faults but will act in the opposite direction for ground faults on B-and-C-phases. Thus, if a directional element with watt-element characteristics is supplied with the zero-sequence current in one coil and the phase-A-component of the negative-sequence current in the other coil, then on a phase-A-to-ground fault this element will have maximum torque in the contact-closing direction. For a fault on phase B the zero-sequence current leads phase A of the negative sequence 120 degrees, and for a fault on phase C the zero-sequence current lags phase A of the negative sequence by 120 degrees. The element will, therefore, have torque in the contact-opening direction for phase-B and phase-C ground faults. If another similar relay element is used with its corresponding coil energized by the zero-sequence current, but its other coil energized by the phase-B component of the negative sequence instead of the phase-A, then, for faults on B phase it will be operating at maximum torque and will close its contacts but will have reversed torque for ground faults on phases A and C. Likewise, a third element energized by zero-sequence current and the phase-C component of the negative-sequence current will close its contacts for phase-C-to-ground faults, but open its contacts for faults on the other two phases. The schematic connections for this relay are shown in Figure 3.

The phase-selector relay consists of three elements similar to the conventional directional element. The polarizing coils of all three elements are energized by the zero-sequence current. The other coil on each element is energized by a component on the negative-sequence current obtained from the 3-phase negative-sequence filter. This filter is so connected that the phase-A component of the negative-sequence system is supplied to the phase-A element, the B component supplied to the B element and the C component supplied to the C element.

The complete ground relay equipment consists of a phase selector relay, a three zone reactance ground relay, an auxiliary unit, and a negative sequence network. Figure 3 shows schematically the selector element connections. Since more contacts are required than can be efficiently provided on the sensitive selector elements, small telephone-type relays are utilized. These relays are energized on direct current by the selector element contacts. The selector relay consists of the three negative-zero sequence selector elements, three auxiliary relays, and three compensators designed to provide compensation for the second and third zone elements of the reactance relay.

The reactance ground relay consists of three beam-type reactance elements, a directional element, and a synchronous timer. The trip circuit connections are shown in Figure 4. The auxiliary unit consists of three compensators associated with the first zone reactance element, and three air-gapped mixing transformers to impart the reactance characteristic to the beam elements.

Figure 5 shows the schematic connections of the compensators and one reactance element.

The zero-sequence current is removed from the three main current transformer leads by the 15/5 ratio auxiliary trans-

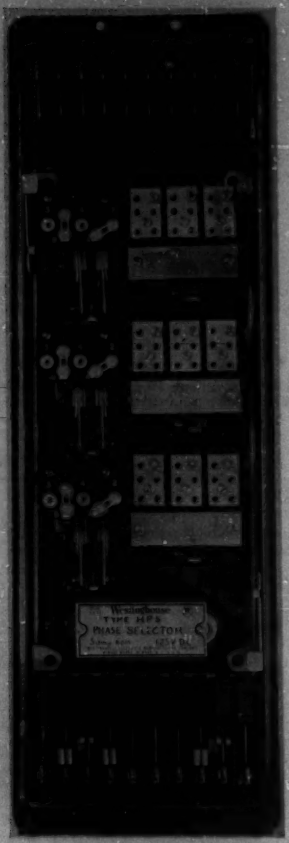

Figure 7. Phase selector relay

formers, leaving only the positive-and-negative-sequence currents to pass through the primaries of the compensators. These compensators consist merely of air-gapped stepdown current transformers. The voltage induced in the secondary winding is almost 90 degrees lagging the primary current. The secondary winding is actually shunted by a variable resistor (not shown) so that, by adjusting the primary and secondary taps and this resistor, the voltage across the secondary can be made exactly proportional to, and the same phase angle as the voltage drop produced by the positive-and-negative-sequence current flowing in the line impedance between the relay and its balance point. The compensator voltage subtracts from the potential transformer line-to-ground voltage so that the resultant voltage applied to the relay coils is the zero-sequence voltage drop $I_0 Z_\phi$. The particular compensated phase-to-ground voltage to be applied to the element is, of course, determined by the action of the phase selector auxiliary contacts T_A, T_B, and T_C, Figure 3.

In order to obtain a reactance characteristic from a beam-type element, it is necessary to balance the vector sum of the current and voltage on the operating or contact end of the beam against a voltage restraint on the back end of the beam. Also, the voltage ampere turns on the operating end must be shifted 90 degrees leading or the current lagged 90 degrees with respect to the voltage, and furthermore, the mechanical pull of the operating end voltage without current must equal the restraint voltage pull. These requirements are met in the circuit shown in Figure 5. The induced voltage in the secondary of the mixing transformer lags the primary current by 90 degrees and this current derived voltage and the compensated line-to-ground voltage are directly combined in the single operating coil on the reactance element. It is permissible to mix the current and voltage circuits in this fashion, since the current from the current circuit is stepped down to the order of magnitude of the current produced by the voltage circuit and the secondary impedances of the mixing transformer, the compensators and the potential transformers are relatively negligible.

During normal unfaulted conditions the reactance element is totally de-energized. When a single-phase-to-ground fault, for instance, a phase-A-to-ground fault occurs the negative-zero sequence phase selector operates to close the S_A make contacts while the S_B and S_C back contacts remain closed, thus energizing the T_A auxiliary relay. This connects the phase-A-to-ground compensated voltage to the element. The zero-sequence current, of course, appears in the element the moment the fault strikes since there is no switching in the current circuit. During the time interval after the fault and before one of the contacts T_A, T_B, or T_C has closed, the only path for the zero sequence current is through the operating and restraint coils in series. This current produces a net restraining torque since the number of turns in the restraint coils is larger than the number of turns in the operating coil. As soon as one of the selector contacts is closed, the proper

Figure 8. Reactance ground relay

voltage is established across both coils and a path through the compensator and potential transformer is established which allows the proper zero sequence current to flow through the operating coil.

The directional element utilizes zero sequence in one coil and the proper uncompensated line-to-ground voltage as determined by the action of the phase selector. These quantities rather than zero sequence current and voltage have been used in order to overcome a characteristic of the phase selector on two phase-to-ground faults. On a 2-phase-to-ground fault, for instance B to C to ground, the zero sequence current is in

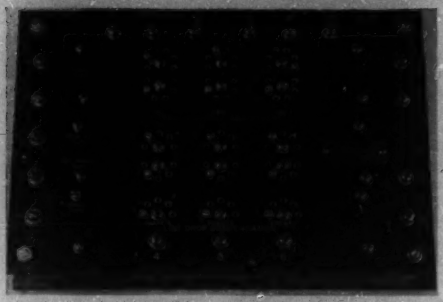

Figure 9. Auxiliary unit for ground relay

phase with the phase A negative-sequence current which is the same relation as is obtained on a phase-A-to-ground fault. The phase selector, therefore, applies the phase-A voltage to the relay on the B-phase to C-phase-to-ground fault. As will be seen from Figure 6, the zero-sequence current in this case is leading the phase-A to-ground voltage and this, of course, causes the reactance element to overreach considerably. Undesirable tripping because of the overreach of the reactance element is prevented by the fact that when the phase-A voltage is applied to the reactance element, another contact (not shown) on the auxiliary relay T_A applies the phase-C-to-ground uncompensated voltage to the directional element. As will be seen from Figure 6, this places the current and voltage on the directional element in the nontrip area. However, on a bona fide phase-A-to-ground fault the same voltage is applied to the directional element, but tripping is permitted because the zero-sequence current has reversed about 180 degrees with respect to the phase-C-to-ground voltage. Thus the relay B, Figure 6 (a), is prevented from tripping for 2-phase-to-ground faults beyond the far bus. Considering now the relay A on the other side of the bus, it is obvious that its directional element will close contacts, since it receives a zero-sequence current reversed from that in relay B. However, its reactance element will measure positive reactance and will receive the unfaulted phase-A-to-ground voltage so, in general, it will not trip on double ground faults beyond bus 2. A close-in 2-phase-to-ground fault as at X may aggravate the situation considerably since there may be enough ground current through the relay A to trip the reactance element even though it is measuring reactance correctly and is restrained by an essentially uncollapsed voltage. When this condition is reached, it can be very simply corrected by the use of an additional directional element energized by zero-sequence current and voltage. However, it is felt that in the great majority of actual applications the use of zero-sequence current and line-to-ground voltage on one directional element to provide a power directional sense and a discrimination between single- and 2-phase-to-ground faults is all that is necessary.

The ground relay equipment previously discussed utilizing a negative-zero sequence phase selector is intended primarily for application on essentially solidly grounded systems. On systems grounded through appreciable values of impedance a voltage-type selector can be used to advantage. Impedance grounded systems generally produce a considerable neutral shift, thus, on single-phase-to-ground faults, the faulted phase to neutral voltage drops to a relatively low value while the other two voltages rise considerably above the normal line to neutral value. A voltage phase selector utilizing one of the unfaulted line-to-ground voltages as an operating voltage balanced against the faulted voltage as a restraint voltage provides a very positive means of selecting the faulted phase.

A reactance ground relay scheme utilizing voltage phase selector elements has been given extensive laboratory and field tests and has performed satisfactorily. The negative-zero sequence phase selector has, of course, already proved highly satisfactory in connection with single pole tripping and reclosing equipment. It is felt that the new reactance ground relay combined with the sequence operated selector or the voltage selector will open up many new applications for distance relays.

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Discussion

W. A. Lewis (Illinois Institute of Technology, Chicago, Ill): The paper by Goldsborough represents the culmination of a very long development program. Having been connected with the early stages of this development, I would like to take this opportunity to review briefly part of its early history. The basic principles upon which this relay is based were described in a paper presented in 1931.[1] Many of the principles incorporated in the relay were described in a paper presented before the AIEE in 1932.[2] At the time these two papers were presented the AIEE did not publish in the Transactions, papers which were considered to be of transitory interest. However, at the request of the relay committee, the first of these two papers was revised and re-presented before the AIEE in January 1947, and published in the Transactions for 1947.

The basic difficulty encountered in the early developments of a relay based on these principles was that the measurement of distance by means of a reactance relay was dependent upon the fault resistance during a double-line fault because the current in the fault resistance was not in phase with the current in the relay. One of the relays on one of the faulted phases would indicate that the fault was too close, and the other would indicate that it was too far away. If the resistance in series with the phase conductors predominated, one relay would overreach, and if the resistance in the ground or neutral connection predominated, the other relay would overreach. The only sure method of preventing false operations thus was to prevent either relay from operating on a double-line to ground fault. Methods for doing this which were then available were quite complex and were thus not attractive.

Meanwhile, the development of carrier-current relaying offered sufficient advantages that the carrier relays were developed, and the distance ground relay was pushed less actively. With recent increases in cost, the cost of carrier-current equipment has risen sharply, so that further study of a distance ground relay is now highly justified.

The development of a phase selection

method in connection with single-pole switching for ground faults now provides a satisfactory method of selecting only the faulted phase for a single-line-to-ground fault and for preventing operation of the relay on other types of fault.

The new relay described in the paper seems to overcome all of the previous disadvantages, and to offer considerable promise for a satisfactory high-speed ground relay which is selective and does not change its balance-point appreciably for a large change in generating capacity. Thus, more widespread application of the distance ground relay should now be possible.

A possible extension in this development should also be mentioned at this time. In the relay described by Goldsborough, tripping is accomplished by zero-sequence current, and the components of voltage drop produced by positive-and-negative-sequence currents are eliminated by the compensators. This arrangement will usually prove most satisfactory. However, for ground protection of a circuit at the terminal where a source of ground current cannot always be assured, this relay or any other ground relay responsive to zero-sequence current cannot be depended upon. An alternative arrangement described in reference 1 of the paper is to use the phase current for tripping, and to remove the voltage drops produced by zero-sequence currents from the restraining voltages by means of zero-sequence compensators. If the development described in the paper finds adequate application and use, the alternative development just mentioned may be justified as the solution of special cases not otherwise taken care of. For such a scheme, the selection of the proper phase relay to trip could usually be made by means of a voltage selector.

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A. R. van C. Warrington (General Electric Company, Philadelphia, Pa.): Goldsborough has shown in a very interesting paper how one distance relay can provide ground protection for a 3-phase terminal, but one wonders whether the cost of the distance relay, the selector relay, and the auxiliary box is not comparable with that of three ground distance relays.

The relay system is by no means new, as the reference numbers 1 and 4 of the author's paper will indicate. A single *GCX* reactance relay with the same potential compensation as the authors describe has been in service since 1942 with a perfect record of correct operation.

A similar scheme was used for phase distance relays in Germany before the war wherein the vector difference of two of the phase currents was supplied to the relays and the potential was switched between phases by a selector relay, suitable compensation being provided for the double current effect of a fault involving both the relay phases.

In ground distance relays current compensation is preferable in some cases and potential compensation in others. Potential compensation is preferable only when there is a large amount of reactive current transfer but has the disadvantage of less sensitivity since the relay gets only a fraction of the phase-to-neutral voltage

The idea of having a phase selector which can go wrong on double ground faults and then correcting it by adding a zero sequence directional unit seems to be a rather oblique approach. It would be better to use a correct phase selector in the first place.

Eric T. B. Gross (Illinois Institute of Technology, Chicago, Ill.): Though the basis for distance relaying of single circuit lines has been known for some 20 years, not many applications have been made in the United States. However, a small number of installations has been in operation quite successfully and some of them were described in the literature.[1,2] Since the majority of faults are grounds, this problem deserves wider publicity and Goldsborough's fine paper is another valuable step ahead in the right direction.

The current compensation discussed in equation (4) of the paper seems to have been introduced first in France in 1929 and has been used abroad successfully in many resonant grounded[3] systems. The difficulties encountered with double-line-to-ground faults in effectively grounded systems do not exist in resonant grounded systems since double-line-to-ground faults are simply phase-to-phase shorts. This may be the reason for the wider applications of current compensation schemes abroad when ground fault neutralizer application is the preferred practice. It may be mentioned that schemes were developed to protect against consequent double ground faults (that is, one ground on one phase at one location and subsequently a second ground on another phase at a different location) selectively by switching off one of the two grounds only in order to get all the advantages which may be gained from resonant neutral grounding.[4,5,6]

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Carrier Channels for Telemetering

T. DeWITT TALMAGE
MEMBER AIEE

Synopsis: This paper describes design engineering considerations involved in the application of 68 carrier current terminals at 25 locations used to establish the major portion of a 6,500-mile telemeter and automatic load-frequency control system. Both telephone-line and power-line carrier equipment is used with the latter predominating and consisting of four general types: keyed continuous wave; multitone amplitude modulation; multitone frequency modulation; and frequency shift. The quantity and direction of power flowing in 17 major transmission tie lines between the Tennessee Valley Authority (TVA) and surrounding power systems are telemetered an average distance of 172 miles to an operating office where tie-line loadings are totalized. Based on this net interchange quantity, deviation, from-schedule control signals, on a time-duration impulse basis representing kilowatts, are sent by a frequency-shift power-line carrier system to a total of 12 hydroelectric plants, to automatically regulate the governors of generators in response to load and frequency changes on the tie lines within the limits set by scheduled water releases which are co-ordinated with other objectives of the TVA. Two separate master control carrier channels are used with one serving seven and the other eight hydroelectric generating plants. To provide operating flexibility, three hydro plants are common to both carrier control channels.

It is the purpose of this paper to review the dominant engineering considerations included in the planning and design of a 6,500-mile telemeter and automatic load-frequency control system required to secure maximum benefits from 17 inter-system tie lines located at 12 different geographic points in the Tennessee River valley region as shown in Figure 1.

The electric power systems stretching from the Great Lakes to the Gulf of Mexico and from west of the Mississippi River to the Carolina coast, serve the South Atlantic and central areas of the United States and normally operate interconnected and in parallel. This integrated power pool is made up of 84 companies having a total installed generating capacity of about 18,000,000 kw, with a peak load approaching close to this same value. The TVA system is centrally located in this network and its generation, furnished by 27 major plants,[1] shown in Figure 1, represents about one-eighth of the total. From the standpoint of water releases and power dispatching, the operation of five additional major generating plants, owned by the Aluminum Company of America, has been co-ordinated to provide maximum utilization of water power of the region. These plants have an installed capacity of 311,120 kw and their locations are included in Figure 1. A peak-hour demand of 2,457,000 kw was recently handled by this joint system which now has an installed nameplate capacity of 2,571,000 kw. The combined power system operated under direction of the TVA represents about 14 per cent of the total interconnected pool capacity.

The basic reasons for interconnections are well recognized and can be summed up in two categories: improvement of continuity and quality of electric service; and maximum economic over-all operation of facilities furnished by the participating beneficiaries. Both of these desirable results redound to the benefit of the ultimate users of electric power service. Hourly schedules of energy transfers between systems are jointly established in accordance with applicable contracts to secure mutually advantageous results. For efficient control of generation and schedules, it has been found imperative to furnish the power system load dispatchers with a clear picture of the power flowing at vital points. This is especially true if rapid recovery from system troubles and disturbances is to be expected under present heavy load conditions.

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T. De Witt Talmage is principal communication and signal engineer with the Tennessee Valley Authority, Chattanooga, Tenn.

Statement of Problem

The main parts of the primary problem of realizing the foregoing essential results were:

1. To bring to a central operating office an indication of kilowatts flowing into or out of the TVA's system from each of 12 principal power interchange points, in the most economical fashion.

2. To provide at the central operating office a flexible arrangement for totalizing individual interchange readings by companies, areas, or a combination of both.

3. Based on one or more selected tie-line loadings, to send a master control signal from the central operating office to 12 hydroelectric generating plants, three of which are owned by two interconnected companies, so that existing control apparatus could automatically regulate the generation of power to compensate for variations in the TVA load.

While both (1) and (2) were installed in gradual stages, part (3) was divided into two separate steps. The first step, based on using a minimum of new material, consisted of installing equipment to transmit one master control signal by power-line frequency-shift carrier equipment to seven hydroelectric generating stations, and was placed in service during World War II in order to further the war effort by loading lines to the maximum capacity.

The second step consisted of providing a second master load control signal by a crystal-controlled frequency-shift carrier channel which could function simultaneously with, but independently of, the first in such a fashion that the TVA power system could be operated in two sections. Both steps provide a system which allows any one or more of a total of 46 units installed at 12 generating plants, arranged as described in the section headed Load-Frequency Control Channels, to be used for the automatic regulation of power interchange. These

generating units represent about one-half of the installed capacity of the combined TVA-Alcoa power system.

The TVA'S 6,666-mile transmission system,[2] 293 substations, and 12 power interchange points are shown in Figure 1. Nine of these interchange points involve one transmission line each, two points involve two lines each, and one point consists of four lines. Ten of these 17 transmission lines are rated at 161 kv, five at 115 kv, and one each at transformation points of 132- to 66-kv and 110- to 66-kv.

The average air-line distance from the power system operating office at Chattanooga, Tenn., to the intersystem metering points is 136 miles, the closest being six miles away and the farthest 266 miles. An important objective was to route the telemeter circuits by the most direct and reliable paths. In the case of several tie-line indications, it was desired to route them by the way of strategically-located generating plants, especially suitable for controlling generation such as Wilson, Watts Bar, and Norris hydroelectric generating plants so that automatic control direct from these tie-line loadings would be available when needed. Also, the Wilson Dam operating office was to be furnished with tie-line interchange readings necessary to keep this point continuously informed of the loading on certain intersystem power circuits. As the system was finally laid out, the average length of the 12 tie-line telemeter channels was 172 miles, only 26 per cent longer than the air-line distance. Ninety-three per cent of this mileage is routed over the power-line conductors by carrier channels, the balance being by telephone cable circuits and carrier over open-wire telephone circuits.

The telemetering system in Figure 2 shows the detailed connections of all channels from the metering equipment to the final output of the telemeter receivers into the load-frequency controllers at ten regulating plants. The frequency-shift load-control receiver at Alcoa substation makes automatic tie-line control available to two of Alcoa's hydroelectric generating plants.

Because of shortages of critical material during the war period, maximum use was made of all existing equipment. Five carrier pilot relay channels were in service between points over which one telemeter-reading was required to be transmitted. These ten pilot relay sets were used to transmit a telemeter signal with the protective relays taking precedence in use of the channel. The output of these carrier pilot relay transmitters is approximately 20 watts each and the average length of the channels is 62 miles. A typical power-line carrier pilot relay transmitter-receiver terminal used jointly for telemeter and protective relay purposes is shown in Figure 3.

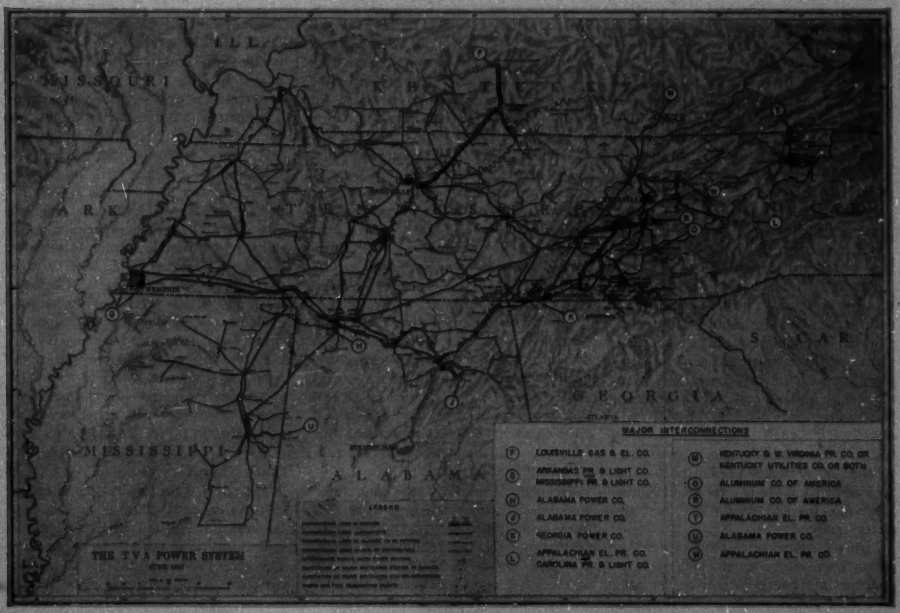
Frequency-Shift Telemeter Channels

In four cases of transmitting one telemeter reading over distances varying from 80 to 100 miles, power-line carrier frequency-shift channels were established following World War II for one or more of the following reasons:

1. No carrier pilot-relay channel was available.
2. Installed carrier pilot-relay channel was already being used for a second service which conflicted with the addition of a telemeter channel
3. Low carrier transmitter power (two

Figure 1. TVA power transmission and distribution system showing 17 major intersystem tie lines at 12 geographical locations

Identification of power interchange points by circled letters corresponds to telemeter code used in Figure 2



watts) was needed to eliminate interference with existing or proposed channels.

Five additional frequency-shift carrier channels are used to telemeter five individual interchange readings from four of the TVA's switchyards to the Commonwealth and Southern Corporation's system[3] and to the Arkansas Power and Light Company. This type of carrier equipment was chosen chiefly because of its ability to overcome high attenuation and noise level since all of these intercompany routes are relatively long, ranging from 120 to 220 miles through complex power networks.

Multitone Carrier Telemeter Channels

From the remaining points on the TVA's system, two or more telemetering readings were required to be transmitted. On all of these routes, carrier line traps and coupling capacitors were available for adding a second frequency by double-tuning the existing line traps and making additions to the line-tuning equipment. Such an installation is shown in Figure 4. These channels were provided by amplitude-modulated carrier equipment with a transmitter power output of 25 watts,[4] using audio tones ranging from 485 to 3,000 cycles per second for individual readings as tabulated in Figure 2. Figures 5 and 10, cabinets 1, 2, and 4, show typical carrier installations of this type.

In one case it was found that the line noise level was high, which interfered with the telemeter service. Over this 120-mile route, unity deviation ratio frequency-modulated carrier equipment[5] having a transmitter power output also of 25 watts was substituted for the original amplitude-modulated equipment, and more satisfactory results were secured. The average length of the eight amplitude-modulated channels is 100 miles.

Figure 2 shows how two of these multitone carrier channels utilize two receivers each, in one case to furnish Wheeler hydroelectric plant, an intermediate generating plant, with telemeter signals simultaneously with the end terminal at Wilson hydroelectric plant, and in the other case, two receivers are used to provide two transfer trip signals required for the protective relay scheme at an intermediate substation, Summer Shade, while the balance of six tones is received at South Nashville, the end substation, for telemeter service.

Figure 2 lists seven plant and company generation readings, six of which are telemetered over multitone carrier channels on a time-duration impulse basis, while the seventh, Chickamauga hydroelectric plant load, is telemetered over a pair of wires in a telephone cable.

Table 1 lists the circuit mileage established by each kind and quantity of equipment for the complete telemeter system totaling 5,688 miles in length, 97 per cent of which is established by carrier channels.

The three vertical rows of tie-line telemeter receiver-recorders on the left side of Figure 6 show the arrangement of instruments on the north wall of the load dispatchers' office at Chattanooga, Tenn. Plant and company generation receiver-recorders are in the right-hand end row.

Power-Line Carrier Frequencies

All carrier telemeter channels are transmitted over the power circuits with the exception of two originating at North Bristol and Nolichucky substations which are routed over open-wire telephone lines as indicated in Figure 2.

The problem of choice of carrier frequencies to use over the power lines was complicated by two factors: (1) many of the carrier telemeter transmitters were located at the edges of the TVA's territory immediately adjacent to or in switchyards owned by others who were using power-line carrier frequencies; (2) the large number of power-line carrier pilot relay[6] and telephone[7] channels in service on all of the TVA's circuits over which the new telemetering frequencies would have to be routed. It was desirable to minimize the changing of operating frequencies, not only because of the cost, but also because of shortage of labor during the War and the difficulty of taking transmission lines out of service to re-tune the line traps.

Based on engineering studies and carrier transmission tests, a carefully worked out frequency allocation plan was made. Thirty-one new frequencies were interspersed with those in operation requiring no changes in existing frequencies. This work involved close co-ordination with neighboring power companies. Nine of these frequencies are modulated by two to eight audio tones, and 22 frequencies are used for the 11 frequency-shift channels. Five carrier frequency-shift receivers are owned by others and are located at Magella substation, Birmingham, Ala.;[3] and at Woodward substation, Pine Bluff, Ark.

Frequencies used by the nine tone-modulated carrier telemeter channels range from 44 to 140 kc. Typical transmitting and receiving equipment of this design is shown in Figures 5 and 10, cabinets 1, 2, and 4. In general, the pilot relay carrier frequencies are located above the frequency band used for telemetering. The communication channels, ranging from 47 to 145 kc, are interspaced with each other and the balance of the services, including the 11 frequency-shift carrier channels, ranging from 37.5 to 131 kc, which are inter-allocated in the narrow interstices.

The frequency-shift equipment is of the crystal-controlled narrow-band type shifting only a total of 24 cycles per second at 37.5 kc and 101.5 cycles per second at 131 kc.

The eight frequencies used for the four frequency-shift telemeter channels within the TVA system are located in the band of 123 to 133 kc which has been reserved for frequency-shift telemetering.

The entire power-line carrier system in operation and under construction in the Tennessee Valley region is in excess of 300 terminals at 70 different points. A total of 160 frequencies is used, ranging from 37.5 to 195 kc.

Carrier Coupling and Trapping

Most TVA power-line carrier telemeter channels are coupled to the "B" (middle) phase wire to ground, which also is the carrier pilot relay channel path. One exception to this arrangement was the two transmission lines between Louisville, Ky.,[8] and South Nashville, Tenn., via Summer Shade where, because of the importance and length of these lines, it was desired to place the pilot relay and telemeter channels on separate phase wires. The carrier pilot relay channel was coupled between phase "B" of each line and ground. Because the continuity of the telemeter readings and transfer trip signals between these points is very important, phase "C" of each line was equipped with coupling capacitors, tuning units, and carrier line traps to establish duplicate circuits. One line is used normally and the other is available for an alternate path. The carrier terminal equipment is switched manually from one circuit to another upon telephone instructions via 2-frequency power-line carrier telephone channels, which are coupled "A" phase to ground on each line. This switching arrangement is shown schematically in Figure 2 at Paddy's Run (Louisville, Ky.), Summer Shade and South Nashville substations.

The phase wires to which all telemeter channels are coupled are trapped at both the transmitting and receiving ends as well as any intermediate by-pass points. This gives maximum stability to the carrier channels, permits most efficient use of the carrier signal, and blocks out noise

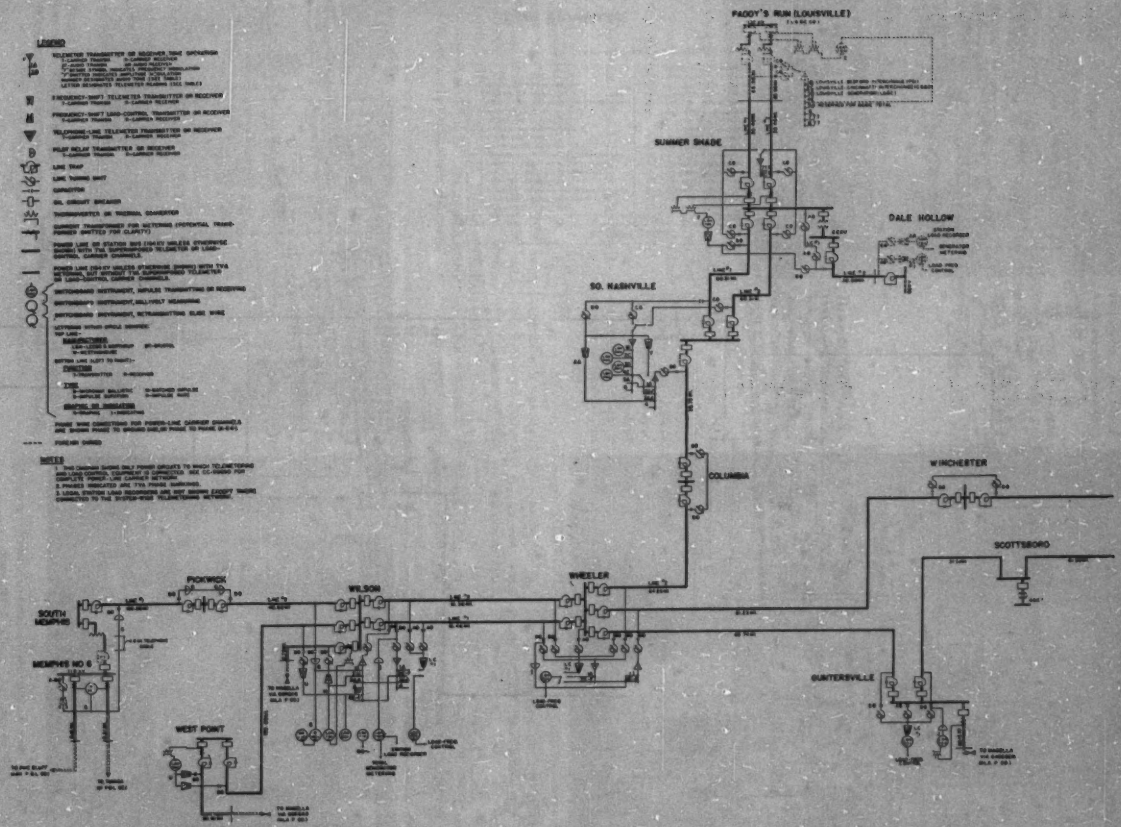


Figure 2. Main elements of carrier equipment and telemeter instruments used to establish 6,500-mile telemeter and load-frequency control systems

Only power and telephone circuits to which these carrier channels are connected are shown (For complete power system see Figure 1)

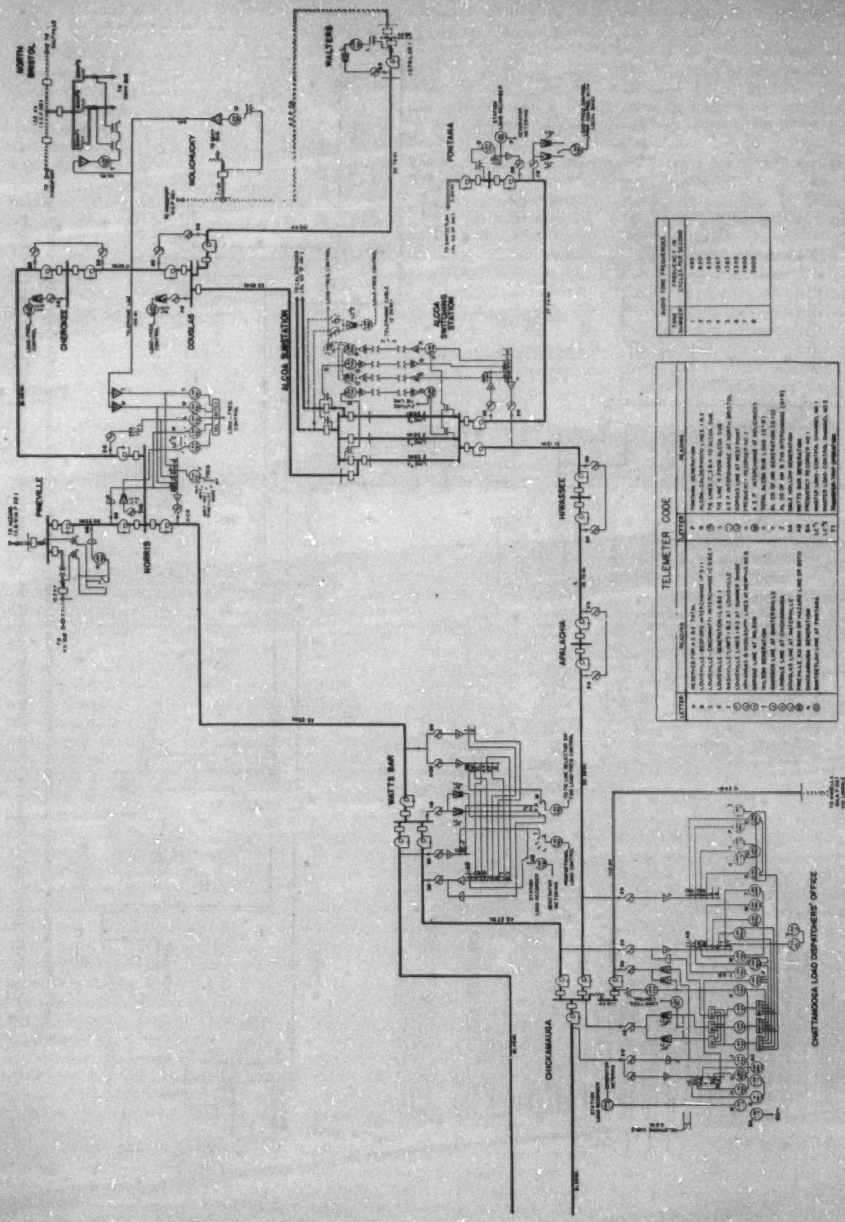


Figure 2 (continued)

Figure 3. Power-line carrier pilot relay transmitter-receiver in 161-kv switchyard at Norris hydroelectric generating plant

This terminal is used jointly for telemeter and protective relay purposes on the Pineville transmission line

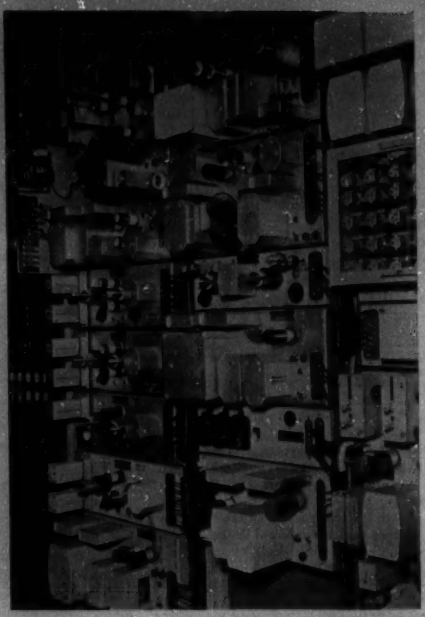


Figure 5. (right) Multitone amplitude-modulated power-line carrier telemeter receiver and transmitter installed on racks at South Nashville substation

Carrier receiver for channel from Louisville, Ky., at left, and carrier transmitter for channel to Wheeler and Wilson hydroelectric generating plants (via Columbia primary substation) on right

from adjacent power circuits. Carrier traps also aid in confining the carrier frequencies to their local paths, thus reducing interference to other frequencies.

Telephone-Line Carrier Channels

Two recently established power interchange points are at North Bristol, Va., and Nolichucky, Tenn. The TVA owns and operates open-wire telephone circuits between these two points and Norris hydroelectric generating plant, over a portion of which is superimposed a group of three carrier-telephone channels. In this case, it was found more economical to use standard telegraph carrier channels for telemetering over the telephone circuits.[9]

This amplitude-modulated telegraph carrier equipment operates above the voice-frequencies on the physical telephone circuit but below the band of six frequencies used in the carrier telephone system. The telegraph transmitter has a power output of 0.004 watt and the frequencies employed are 4.53

Figure 4. Two-frequency carrier line traps in vertical position, coupling capacitors equipped with potential devices (front view), and carrier-frequency connections between capacitors and carrier line-tuning units (rear view) in typical TVA 161-kv switchyard

Telemeter and pilot relay channels are coupled to the middle phase wire to ground; two telephone channels are coupled to outside phase wires

Figure 6. Telemeter receiver-recorders on north wall of TVA load dispatching office, Chattanooga, Tenn.

Principal tie-line load recorders shown in first three vertical rows at left. Plant generation recorders in right-hand end row

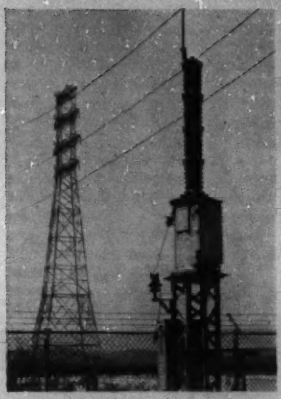


Figure 7. Method of coupling load-frequency control carrier channels to each section of "A" phase of 161-kv busses in Chickamauga hydroelectric generating plant switchyard

Carrier line-tuning unit is in cabinet below coupling capacitor potential device assembly (rear view)

and 4.77 kc. Because this telephone line is made up of three individual circuits, the carrier-telemeter frequencies are bypassed at two intermediate points by standard high-and-low-pass filters. This arrangement permits the use of the three individual physical circuits for unrestricted telephone service.

The TVA's open-wire telephone circuits in East Tennessee also serve Watauga hydroelectric generating plant, which is now under construction. It is feasible to make the North Bristol and Nolichucky tie-line readings available at Watauga by making minor changes and adding carrier and telemeter receivers. However, when this is done, transmission studies indicate that a carrier repeater will be required between Nolichucky and Norris in order to maintain proper received signal strength at Norris.

The TVA's experience with telephone-line carrier for speech transmission, plus duplex telegraph, simultaneously, for printer telegraph service between Chattanooga and Wilson dispatching offices, has been satisfactory. This speech-plus-duplex equipment includes filters for removing from the speech path all frequencies above 2,200 cycles per second. The band between 2,200 and 2,700 cycles per second is employed for transmitting two carrier telegraph channels, on a frequency-modulated basis, at approximately 2,375 and 2,575 cycles per second. Such speech-plus-duplex equipment can be used for transmitting telemeter impulses.

Figure 8. Front view of power-line carrier frequency-shift master control number 2 transmitter (crystal-controlled) at Chickamauga hydroelectric plant, having a 24-cycle-per-second shift between high and low frequencies

Plans to install a direct telephone-line carrier channel for telephone service from Chattanooga to Watauga hydroelectric generating plant include the speech-plus-duplex feature for transmitting the number 2 master load-frequency control signal to Watauga, a distance of about 250 miles. The standard speech-plus-duplex equipment provides transmission of impulses in both directions simultaneously. Thus kilowatt indications of the generation of Watauga hydroelectric plant may be sent back to the load dispatching office at Chattanooga upon the addition of the telemeter instruments proper.

Table I. Telemeter and Load-Frequency Control Statistics, 1948

Kind of Apparatus	Quantity	Channel Mileage
Telemeter:		
Jointly-used power line carrier pilot relay sets	10	306
Power-line carrier frequency-shift transmitters	9	1,191
Power-line carrier frequency-shift receivers	9	
Power-line carrier audio-tone A.M. transmitters	8	3,291
Power-line carrier audio-tone A.M. receivers	10	
Power-line carrier audio-tone F.M. transmitter	1	480
Power-line carrier audio-tone F.M. receiver	1	
Telephone-line carrier transmitters	2	266
Telephone-line carrier receivers	2	
Telephone cable circuits	10	154
Telemeter instruments	63	
Load-Frequency Control:		
Jointly-used power line carrier pilot relay sets	2	49
Power-line carrier frequency-shift transmitters	2	771
Power-line carrier frequency-shift receivers	12	
Telephone cable circuits	3	20
Telemeter instruments	15	
Totals:		
Carrier transmitters and receivers	68*	6,528
Telemeter instruments	77**	

* Ten carrier sets are owned by others.

** Eight telemeter instruments are owned by others.

Figure 9. Rear view of power-line carrier frequency-shift master control number 2 transmitter, showing arrangement of component parts and wiring

Load-Frequency Control Channels

The master load-frequency control carrier channels, by addition of a carrier lead-in bushing and cable, made use of existing coupling capacitors that were in place to obtain potential in the hydroelectric generating plant switchyards constructed by the TVA and which are connected to phase "A" of each section of the main and transfer busses as shown in Figure 7. Exceptions made for reasons of economy are at Wilson where the control channels were coupled to existing capacitors on both Wheeler lines and at Fontana where both control receivers were connected to the line capacitor also used for establishing a carrier telephone channel.

Figure 10. Multitone amplitude-modulated power-line carrier telemeter equipment: receivers in cabinets 1 and 4, transmitter in cabinet 2

Frequency-shift carrier receiver for automatic load-frequency control in cabinet 3 at Watts Bar hydroelectric generating plant

Figure 11. Totalizing telemeter recorders and telemeter transmitters on south wall of load dispatching office, Chattanooga, Tenn.

Frequency recorders shown at bottom of center row and top of right row

The frequencies for the two master control channels are spaced 300 cycles apart, and the total shift of each pair of frequencies is 24 cycles per second. These two signals are sent out to the regulating plants over the three 161-kv circuits radiating from Chickamauga hydroelectric generating plant.

The first master control channel, placed in service in 1944, was not crystal controlled and it had a total shift of 600 cycles per second. The addition of crystals to this equipment at the time the second master control channel was installed permitted the narrowing of the channel to a shift of only 24 cycles per second, and operating the two pairs of frequencies much closer together than would otherwise be possible with the attendant saving in use of the carrier frequency band.

At Summer Shade substation, it was necessary to by-pass the number 1 control channel from the 161-kv system to a 66-kv line, as shown in Figure 2.

Continuity of the control channels is maintained uninterrupted under different switching conditions by the use of alternate parallel carrier paths. To date, no trouble has been experienced with the load-frequency control carrier receivers due to multiple-path transmission of carrier frequencies.

The two master control frequency-shift carrier transmitters at Chickamauga hydroelectric generating plant make use of a common 25-watt amplifier, line-tuning units and coupling capacitors. The front and back views of one of these crystal-controlled transmitters, rated at 2 watts output, are shown in Figures 8 and 9, respectively. A frequency-shift carrier receiver for load-frequency control purposes is shown in cabinet 3, Figure 10.

Table I lists the quantities of carrier and telemeter equipment required to construct these two control circuits totaling 840 miles in length, 97 per cent of which is established by carrier channels.

The seven regulating hydroelectric generating plants served by the number 1 master load-frequency control channel consist of 33 generating units with an installed capacity of 939,700 kw. The number 2 master load-frequency control channel serves six hydroelectric plants having 16 units with an installed capacity of 586,800 kw and, in addition, serves the Aluminum Company of America in such a fashion that they can re-transmit the signal to two of their hydro plants with an installed capacity of 197,500 kw in seven units.

From Figure 2, it will be noted that Chickamauga, Fontana, and Watts Bar hydroelectric plants with an installed capacity of 366,000 kws in ten units have access to both load-frequency control channels. This is to allow flexibility to meet varying operating requirements.

Figure 11 shows the location of the master load-frequency control telemeter transmitters in the dispatching office at Chattanooga, Tenn., together with other totalizing telemeter recorders and two system frequency recorders. Master control transmitter number 1 is in the left-hand vertical row and the master control transmitter number 2 is in the right-hand vertical row. The recorder used for totalizing purposes in the center vertical row is equipped with an impulse transmitter, and this instrument can be used when either one or the other of two master transmitters is out of service.

Power system frequency in the Chattanooga-Chickamauga area is shown on frequency recorder number 1. The frequency at Norris hydroelectric generating plant is shown on frequency recorder number 2. The Norris frequency is telemetered to Chattanooga via Watts Bar over two power-line carrier telemeter channels in series as shown in Figure 2. Figure 10, cabinets 2 and 4, shows the intermediate carrier terminal equipment used for this service along with several other readings which are transferred from one carrier channel to another. System frequency is used to bias tie-line loading in regulating generation.

The majority of the telemeter transmitters, proper, which actuate the carrier equipment are of the time-duration type and incorporate the Micromax principle of operation. The legend of Figure 2 shows the details of each telemeter transmitter and receiver which is mounted on the main switchboard panels of the substations and hydroelectric generating plants, and in load dispatching offices. Each telemeter receiver is arranged to give an alarm upon failure to receive a carrier signal or upon reception of a continuous carrier signal.

The six switches required for totalizing and choosing control channels are shown in the center of Figure 6, located between the two twin rows of receiver-recorders. A very flexible system of test blocks in the

millivolt circuits and the impulse circuits, external to the telemeter instruments, is available at Chattanooga for making special or emergency setups as well as to expedite maintenance work.

Four tie-line readings in the eastern part of the TVA's system are not only routed through Norris hydroelectric generating plant but are made available by telemeter-receivers which, by means of a selective switch, permit the use of any one or more of these readings for automatic load-frequency control when needed. This arrangement permits maximum flexibility to meet a variety of operating conditions which arise.

The generation of Wilson hydroelectric plant is telemetered to the Chattanooga load dispatching office via Watts Bar. Switches are provided at Watts Bar as shown in Figure 2 to permit using the reading sent from Wilson with a proportional load controller which allows the Wilson and Watts Bar hydroelectric plants to operate in parallel and carry a definite proportion of the regulating load.[10] This proportional control arrangement was completed in 1946.

All of the carrier equipment for telemeter and load-frequency control service is located indoors with one exception in the eastern portion of the territory where a frequency-shift telemeter transmitter is located outdoors in a switchyard. Temperature-controlled crystals are used in this carrier transmitter. Crystals used in indoor carrier apparatus are not temperature controlled.

Each telemeter and load-control circuit is connected to standard telephone twin jacks so that by means of standard telephone patch cords these channels can be routed over alternate paths in emergencies or for special operating setups. Carrier pilot relay channels which are normally used jointly for telemeter and relay purposes, as well as many that are not regularly so used, have been likewise connected to telephone twin jacks permitting the use of these channels in either direction.

The carrier pilot relay sets operate from 125- or 250-volt d-c control batteries, but the balance of the carrier terminals operate from 115-volt a-c station service. At all points where a-c operated telemeter and load-control carrier sets are in service, automatically-operated emergency power supplies are available which have assisted in securing a high degree of continuity of service from the carrier channels.[6]

Summary

The co-operation among members of the interconnected power systems in the southeast region in making joint carrier tests and studies and taking part in intercompany engineering discussions on the assignment of power-line carrier frequencies has been an invaluable aid in designing channels without the use of special carrier equipment.

Careful planning and engineering, giving due weight to economies and future growth, are necessary to make joint use of common power-line carrier equipment by two operating systems, especially if both parties already are using carrier channels.

The use of carrier line traps has allowed the establishment of a far greater number of carrier channels than would otherwise be possible. The use of these traps, low transmitter power, and judicious choice, spacing, and location of frequencies have kept intercompany interference to a minimum. When the few intercompany frequency conflicts have arisen, prompt corrective measures have always been taken on a co-operative basis.

A study of Figure 2 will show that the several different types of standardized carrier equipments available lend themselves to a large number of arrangements and combinations which provide a high degree of flexibility and operating reliability to insure maximum efficiency in generating power.

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Discussion

C. W. Boadway (The Hydro Electric Power Commission of Ontario, Toronto, Canada): The over-all description of the telemetering and load control facilities used by the TVA was quite complete. It was shown how particular problems with an amplitude modulated multitone system were overcome by the use of multitone frequency modulation and frequency shift. This may prove valuable to present installers of this equipment.

It might be interesting for the author to explain the type of transmission used for relay signals, and further to emphasize the use of protection for power lines between the relay carrier current terminals. We assume that relaying in itself is a complete system in addition to the telemetering network described.

The author has clearly defined various installed uses for telemetering and load control as provided by power line carrier, telephone line carrier, open-wire physical circuits, and lead-covered telephone cables.

E. W. Kenefake (General Electric Company, Syracuse, N. Y.): It is certainly interesting to note the diversity of types of carrier-current equipment used on the TVA system for telemetering channels. It is indicative of the variety of application requirements which face a system communication engineer. The paper also leaves the very definite impression that the system has been well engineered and a credit to the people responsible for the planning. To others, it is certainly an excellent proving ground for various types of telemetering channels, which contributes valuable information as to the proper channel to use for given set of conditions. This is interesting to the manufacturer as well as the user. Perhaps the author will discuss the application advantages of various types of channels in a subsequent paper.

It is worthy to note that the TVA have converted amplitude-modulated tone equipment to frequency-modulation for reasons that arose in a particular application. As far as I know, that is the first time frequency modulation has been used for the transmission of multitone telemetering channels. Here is a case where the user has pioneered in the use of one of the various types of modulation for telemetering channels.

It is also evident that considerable planning and engineering have gone into the allocation of frequencies on the system, I am sure the techniques developed in this

case would be useful to the industry in general. I would like to encourage the author or one of his associates to prepare a paper on their method of allocation of carrier frequencies for some subsequent technical meeting.

T. De Witt Talmage: C. W. Boadway inquires about the co-ordination of the pilot relay carrier channels and telemeter service. He is correct in his assumption that the pilot relay carrier system is separate from the telemeter carrier system, with the exception of ten pilot relay terminals which are used jointly for both services. The pilot relay system, initially described in a previous paper,[1] has been more than doubled in size since that date. Ten per cent of the pilot relay carrier terminals now in operation are used normally for dual purposes, the continuous-wave carrier transmitters being keyed by telemeter auxiliary relays. Most of the carrier line traps originally installed for pilot relay service have since been double-tuned to block a second frequency which is used to establish a separate telemeter channel.

E. W. Kenefake emphasizes that certain phases of carrier application problems are of current interest to the industry and justify future discussion, namely, the method of allocating power-line carrier frequencies, and the application advantages of various types of channels. The subject paper describes the resulting pattern of using different kinds of channels and shows that a wide variety of channels can be co-ordinated for one particular type of service. The availability of carrier frequencies was, of course, an important consideration. As more experience is secured, the record can be enlarged accordingly.

Reference

1. Carrier-Current Relaying and Communication on the TVA System, **M. Stephens Merritt, T. DeWitt Talmage.** Electrical Engineering, volume 61, November 1942, pages 561–72.

Feeder Voltage Regulator Application on Power Systems

J. N. GOSINSKI
ASSOCIATE AIEE

J. R. OBERHOLTZER
ASSOCIATE AIEE

Synopsis: Feeder voltage regulators when properly applied will permit the optimum in economical design of power supply systems. The system designer must recognize definite relationships between the available range of regulators and the required voltage variations imposed by voltage drops in the subtransmission system, substation transformers, distribution feeders, and distribution system beyond the feedpoint. Since the effective range of regulators is reduced by inaccuracies in their control equipment, it appears justifiable to specify regulators with a total error of ± 1 per cent.

THE economical design of power transmission and distribution systems requires the proper allocation of voltage drops between the point of supply and the ultimate customer's service entrance. The judicious application of distribution voltage regulators is a normal requirement in the design of the usual type of power systems. They assist in obtaining optimum loading of the subtransmission and distribution systems and the furnishing of adequate customer service voltage. The use of distribution voltage regulators is particularly associated with systems where voltage variation and voltage drops determine the capability of the circuits. This paper describes the application of distribution voltage regulators under such conditions and their functions as related to other elements of system design.

Utilization Voltages

First, consider what values of voltage are desirable for customers' utilization. The joint Edison Electric Institute-National Electrical Manufacturers Association committee on preferred voltages has been studying this matter for a number of years and is issuing a report Preferred Voltage Ratings for A-C Systems and Equipment* which contains recommended voltage limits. Table I from this committee's November 1946 report[1] is reproduced herein in part as Table I for ready reference.

The "favorable zone" (125-110 volts) defined in the report is intended to include the majority of the utilization voltages in customers' premises. Quoting from the report, "Equipment will ordinarily be designed and rated so as to give fully adequate and efficient operation throughout this zone (although not necessarily with guaranteed characteristics at all voltages)." A reasonable maximum value for the drop in interior wiring is given in the Appendix of the EEI-NEMA report as three volts. Thus, a customer's service voltage range equivalent to the favorable zone listed above may be taken as 125-113 volts.

Electrical distribution systems should be designed and are normally operated in such a manner that service entrance voltage variations will be within limits which will provide proper utilization voltages at the customer's equipment. Designers of equipment recognize that voltage variations will exist and make provision for them in the operating characteristics of the equipment. The suggested values which have been set up by the EEI-NEMA committees, after exhaustive investigation of the practices of operating companies and manufacturing organizations, represent the optimum in balancing economical distribution design with economical utilization equipment design so as to provide the customer with satisfactory operation at minimum over-all cost.

Function of Distribution Voltage Regulators

The function of distribution voltage regulators on power systems is to compensate for voltage variations and voltage drops in the subtransmission system, the substation transformers, and the distribution system, so that the customer's service voltage may be held within acceptable limits.

On the usual subtransmission system the voltage at full load often will be lower than that at light load and the voltage drop through substation transformers will be greater at full load than at light load. These variations must be counteracted by the distribution voltage regulator. Of course, it is possible to design a power supply system such that there are no voltage variations which add to the duty of the regulator (zero regulation), and even such that variations are deliberately produced which will compensate for the substation transformer drop and provide the required distribution circuit primary voltage variations for proper customer service voltages as outlined below. In such a case, regulators may not be required. However, other considerations than exact control of the distribution voltage usually determine the subtransmission voltage variations.

Obviously it is not economical to design distribution circuits without voltage drop. With the use of distribution regulators, the design drop on an overhead urban

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J. N. Gosinski and J. R. Oberholtzer are both with the Commonwealth and Southern Corporation, Jackson, Mich.

distribution system from the primary at the first customers' transformer (feed point) to the customer entrance on the most distant service from the last transformer may be as much as 9 per cent or 11 volts on the 120-volt base and still stay within established voltage limits. Experience and the result of studies in economic distribution system design have indicated definite desirable apportionment of the voltage drop beyond the feed point among the various individual components, that is, primary, transformer, secondary, and service as follows (120-volt base):

Primary circuits, first to last transformer	3 volts
Distribution transformer	$3^1/_2$ volts
Secondary	$3^1/_2$ volts
Service	1 volt

The optimum service entrance voltage of 120 volts can be obtained by a contact-making voltmeter (CMV) setting of 120 volts and a compensator setting of the full load voltage drop to the feed point plus five volts. Then the regulator will provide a full load voltage at the feed point of about 125 volts and a light load voltage of 120 volts plus the ratio of light load to full load multiplied by five volts. At full load the regulated feeder voltage at the substation will be higher than the voltage at the feed point by the amount of the feeder drop. The same will be true at light load.

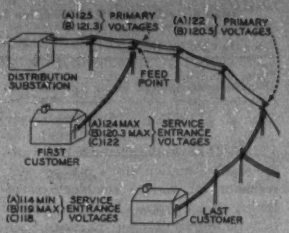

Figure 1. Urban distribution voltage levels with various load conditions

(A) voltage at full circuit load
(B) voltage at light circuit load
(C) mode voltage at full circuit load

Figure 1 shows the various extremes of voltage which may appear on a typical properly regulated distribution system from the feed point to the last customer. Light load was assumed to be 25 per cent of full load for this diagram. For full circuit load conditions, the maximum service voltage of the first customer and the minimum service voltage at the last customer are shown to be 124 volts and 114 volts respectively, a maximum spread of 10 volts. The voltages which will result under light load conditions for these same locations are also shown. It is apparent that both the full load and the light load voltages at these locations are within the favorable zone. The extension of regulator compensation beyond the feed point gives particular advantages in maintaining the majority of customers' voltages within narrow limits from full load to light load. Because regulated design primary voltages are greatest at full load when transformer-secondary drops are generally greatest and conversely are lowest at light load when transformer-secondary drops are least, the net service voltage variation is held to a minimum.

Also shown on Figure 1 are the mode voltages at these locations under full load conditions. These mode voltages are the values which will occur during the greatest percentage of the full load period and will be an important factor in the life and over-all satisfactory performance of equipment. The full load mode voltage at the service entrance for the closest customer normally will be about 122 volts and for the most distant customer about 118 volts. Allowing 1.5 volts for the average interior wiring drop, the utilization voltage modes will then be 120.5 and 116.5 volts respectively for the customers at the

Table I. System Voltages

(Reproduced from EEI-NEMA Report)

Ratio of Preferred Nominal System Voltage to 120	Preferred Nominal System Voltage*	Voltage Zones: Favorable Zone Minimum	Favorable Zone Maximum	Tolerable Zone‡ Minimum	Tolerable Zone‡ Maximum
	Voltages at Point of Utilization by Equipment				
1/2	120/240	110/220	125/250	107/214	127/254
1/1.73Y	120/208(Y)	114/197(Y)	125/217(Y)	111/193(Y)	127/220(Y)***
2	240	210	240	200	250
4	480	420	480	400	500
5	600	525	600	500	625
20	2,400	2,200	2,450	2,100	2,540
20/34.6Y	2,400/4,160(Y)	2,200/3,810(Y)	2,450/4,240(Y)	2,100/3,630(Y)	2,540/4,400(Y)
40	4,800	4,400	4,900	4,200	5,080
60	6,900**	6,300	6,900	6,000	7,200
	7,200**				
	Primary Voltages at Distribution Transformers				
20	2,400	2,300	2,500	2,200	2,600
20/34.6Y	2,400/4,160(Y)	2,300/4,000(Y)	2,500/4,330(Y)	2,200/3,810(Y)	2,600/4,500(Y)
40	4,800	4,600	5,000	4,400	5,200
60	7,200	6,900	7,500	6,600	7,800
40/69.3Y	4,800/8,320(Y)	4,600/8,000(Y)	5,000/8,660	4,400/7,620(Y)	5,200/9,000(Y)
100	12,000	11,000	12,500	10,500	13,000
60/103.9Y	7,200/12,470(Y)	6,900/12,000(Y)	7,500/13,000(Y)	6,600/11,450(Y)	7,800/13,500(Y)
63.5/110Y	7,620/13,200(Y)	7,270/12,600(Y)	7,960/13,800(Y)	7,000/12,100(Y)	8,250/14,300(Y)
110	13,200	12,600	13,800	12,100	14,300
120	14,400†	13,000	14,500	12,600	15,000

(Y) designates wye-connected.

* Except for the first line, which indicates the usual single-phase 3-wire systems, the figures in the second column refer to 3-phase systems. Single values designate systems on which single-phase equipment is ordinarily connected from phase to phase; double values designate systems on which single-phase equipment is ordinarily connected from phase to neutral.

** Since utilization at this level is confined principally to 6,600-volt motors in large steel mills, mines, and so forth, a nominal designation of 6,900 volts is commonly used and is included herewith, even though it is not consistent with other nominal voltages in the utilization group. The voltage zones for this level are likewise related to 6,900 volts rather than the 7,200 volts.

*** Equipment to be used on both the 120/208Y and the 240-volt systems must recognize the minimum voltage of the former and the maximum voltage of the latter.

† 14,400 includes many systems which have been called 13,800, and the voltage zones are correspondingly lower.

‡ Equipment designed for the Favorable Zone will in general give fairly satisfactory operation throughout the Tolerable Zone, except that modification of loading or alternate designs may be required for certain types of equipment.

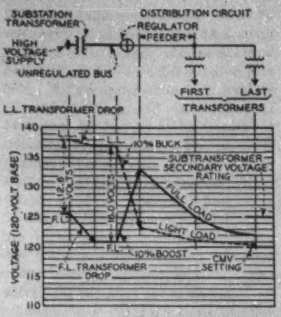

Figure 2. Light load and full load voltage profiles

extreme locations on the system. Since the greater number of customers are located between the feed point and the end of the secondary system, the prevailing full load mode utilization voltage will be about 118 volts.

Thus the required range of the voltage regulator will be determined by three components: the requirements for correction of the variation in subtransmission system voltages and substation transformer voltage drops: the requirements for compensation of the voltage drop in the distribution feeder; and the regulation required beyond the feed point. If both a long and a short distribution feeder are supplied from the same substation, the minimum unregulated bus voltage which can be corrected by a regulator of a given per cent range will be determined by the full load requirements of the long feeder and the maximum light load unregulated bus voltage by the light load requirement of the short feeder. As shown on Figure 2, with normal distribution circuit design, the use of a ±10 per cent feeder regulator permits positive regulation on the high voltage supply of about 10 per cent. This permissible regulation makes it possible to save plant investments in subtransmission supplies for many substations.

Table II. Allowable Subtransmission Supply Variation ±10 Per Cent Regulators

(120 Volt Base)

Feeder drop, volts	0	4	8	0 and 4	4 and 8	0 and 8
Allowable subtransmission supply variation, volts	18	15	13	14	12	10

Table II shows the allowable high voltage supply variation with a ±10 per cent regulator for various combinations of feeder voltage drops. Full load substation transformer drop was taken as 4.5 per cent and light load was assumed to be 25 per cent of full load.

Table III indicates the regulator range which will be required for various combinations of power supply voltage regulation and distribution feeder drops. This illustrates the beneficial effects, in reducing the regulator range required, which are introduced by a negative regulation on the supply side of the substation.

The kilovolt-ampere capacity of the regulator usually is determined by the capability of the distribution circuit. Grouping of circuits for a single large regulator often is feasible when the load shapes are identical, such as commercial loads. Because the load factors may be 30 to 50 per cent, it may be satisfactory for the regulator to operate from 120 to 140 per cent of name plate rating during peak periods. Single-phase regulators are normally used on substations to supply radial distribution circuits when phase unbalance must be considered. Three-phase regulators are preferable because of their lower costs per kilovolt-ampere on systems on which a phase balance can be maintained, such as commercial circuits and primary networks.

Accuracy of Regulator Control

The actual voltage which the customer receives is determined entirely by the accuracy of control by the voltage regulator. Only recently has regulator accuracy been given the consideration which it deserves. While less accurate regulators may be lower in price it has been found that one per cent regulator accuracy is entirely justifiable on the basis of the improved results.

The control equipment for feeder voltage regulators consists essentially of a voltage responsive relay and a source of potential or "control voltage" which is applied to the terminals of this relay and which indicates the voltage at the regulator output terminals. The control voltage causes the relay to operate in response to variations in the terminal voltage such that this voltage or the voltage at some other specified point in the system is maintained within certain limits. A device called a "line drop compensator" simulates the voltage drop throughout a portion of the circuit being regulated and this voltage is introduced in series with the regulator voltage applied to the control element. The regulator is thereby enabled to maintain the desired voltages at points remote from the point of installation. The small inherent difference between values of control voltage applied to the relay which cause the "raise" and "lower" contacts to operate is known as the relay band width.

The effectiveness of the entire regulator is directly dependent on the accuracy of the operation of the control devices. Performance of voltage regulators which cannot be exactly predicted due to inaccuracies of their control devices may require excessive system investments to maintain satisfactory utilization voltages throughout the system. Errors in the control device will limit the availability of the full range of the regulator. For example, if the total accumulative error in the control equipment is ±2.5 volts and the relay band width is ±1 volt, the total error will be about 7 volts and the regulator may be considered to be only about 70 per cent effective. Of course, this assumes that all of the errors are accumula-

Table III. Regulator Range Requirements

(120 Volt Base)

Distribution feeder drop, volts	0						4						8					
Regulation on supply side of distribution substation, volts	−4	0	4	8	12		−4	0	4	8	12		−4	0	4	8	12	
Regulator voltage range required*	3**	7	11	15	19		6	10	14	18	22		9	13	17	21	25	
Regulator range required, per cent	0	±5	±5	±7½	±10		±5	±5	±7½	±7½	±10		±5	±7½	±7½	±10	±10	

* These figures include 3.7 regulator volts required for the distribution system beyond the feed point and 3.3 regulator volts required for the distribution substation transformers.

** This range is borderline. If the distribution substation transformer drop is small, no regulators are required. However, high transformer drop or some allowance for additional distribution circuit drop would indicate the use of ±5 per cent regulators.

tive and occur at the same time, and that the errors operate to limit the buck as well as the boost operation of the regulator. Some idea of the probable cost of additional system strengthening which may be required when designing for this degree of error can be obtained from analysis of the investment in transformer-secondary combinations on residential circuits designed for different voltage drop limits. The additional cost of the design for one volt less drop is in the order of $3.00 per kilovolt-ampere of diversified circuit demand for 4,160-volt circuits and in the order of $4.00 or more per kilovolt-ampere for 7,200-volt circuits. It must be realized that ordinarily there are about 10 volts which can be expended in the form of voltage drop in the design of a distribution circuit. Then each volt which is lost due to inadequate regulator performance will represent one volt out of ten in the measurement of increased investment required. It seems that an over-all improvement in the economics of distribution may reasonably be effected by investing more money in regulators of improved performance characteristics and thereby afford a greater saving in the distribution circuit itself. It appears that a limitation of errors to a total of about one per cent in the control equipment of a regulator is entirely justified by the additional useful range which will be realized.

Conclusions

The opinions expressed in this paper are based on experience with several power systems supplying an aggregate total peak demand of approximately 2,000,000 kilowatt-hours/hour on which system investment has been minimized by the use of feeder voltage regulators.

The most economical over-all power system design will result when the limitations on customer voltage variation are met by the use of feeder voltage regulators of specified accuracy. Distribution system designs have been evolved which make full use of this accuracy of voltage control.

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2. Accuracy of Control Devices for Feeder Voltage Regulators, AIEE Subcommittee. Electrical Engineering, volume 63, July 1944, pages 250–54.

3. Coordination of Distribution and Power Supply System Voltages, C. E. Arvidson. Minutes 28th meeting, Electrical Equipment Committee, Edison Electric Institute (New York, N. Y.), May 5, 1942.

Discussion

W. E. Birchard (General Electric Company, Pittsfield, Mass.): Gosinski and Oberholtzer have presented a factual appraisal of the application of feeder voltage regulators to typical power systems. Their evaluation of savings in investment amounting to $5,000 to $10,000 per feeder for each per cent improvement in regulator accuracy is very impressive compared to the small additional cost for high-accuracy regulators. This phase of distribution economics should be of interest to many distribution engineers.

Power System Voltage Control Without Feeder Voltage Regulators

N. N. SMELOFF
ASSOCIATE AIEE

ONE of the most important phases of power system planning and operation is the provision of required voltages for the various loads throughout the system to insure successful use of electricity and to promote its use for additional applications. However, provision of better voltage than necessary is not economically sound since voltage is a rough indication of the ratio between system capability and the size of the load.

Thus the broad problem is to provide enough capacity for adequate voltage control at the minimum over-all cost. At the same time, of course, the facilities provided must fit into the broad patterns for development of the system through the years.

It is the general practice of Pennsylvania Power and Light Company to provide adequate voltage to its customers without the use of feeder voltage regulators. This paper presents the reasons for this and the methods used.

Voltage Requirements

Before proceeding with a description of the control methods used by the company, a short discussion is required to explain what is meant by "adequate" voltage. It is obvious that a customer's equipment will not operate properly with, say 80 per cent nominal voltage applied to it. Lights will be dim, heaters will work slowly, and motors may not start. On the other hand, 120 per cent nominal voltage will result in reduced life of equipment. Thus it is necessary that the voltage supplied to the customer be at the right level to operate his equipment properly.

The second condition for adequate voltage is that the changes must not be such as to have a detrimental effect upon the operation of utilization equipment. An occasional voltage fluctuation of several per cent is not objectionable, but a repetitive voltage fluctuation of even $1^1/_2$ per cent causes annoying flickering of lights if the frequency is right. On the other hand, a slow change in voltage, even relatively large, may not be noticed.

Pennsylvania Power and Light Company System

A brief description of the company's system and territory is necessary to make clear the reasons why feeder voltage regulators are not generally used for voltage control. The area served is about 9,300 square miles in the center of eastern Pennsylvania with a widely dispersed population of about $1^3/_4$ million. (See Figure 1.) Out of the 774 communities with populations over 100 served by the company, only eight are larger than 20,000 and the largest is only about 100,000. The peak load is over 600,000 kw and the annual energy consumption is over $2^3/_4$ billion kilowatt-hours. Customers are served from any line of the four standard system voltages of 66 kv, 12 kv, 4 kv or 120/208 volts if the capacity available is adequate. Choice of voltage is based only upon the size of the load and the availability of suitable lines. No loads are tapped directly from the 220-kv lines which are used exclusively for the transmission of bulk power.

The power supply system, shown in Figure 2, consists of a 66-kv network of lines, fed at strategic points from large generating stations and a 220-kv transmission system. While there is some local distribution from generator busses, the bulk of the power is delivered directly to the 66-kv system, from which transformation is made for supply directly to large power customers as well as to the standard distribution voltages of 12 kv and 4 kv.

The significant feature of the system from the voltage control standpoint, is that customers are served from any of the standard voltages except the 220 kv. This practice results in an unusually large number of 66-kv substations as is illustrated in Figure 2.

Few if any systems, as a general practice, provide voltage regulators for any unit smaller than a feeder. In fact, many systems provide only bus voltage regulators and adjust the feeder lengths and loads to give adequate voltage on all feeders from a single bus.

The company's 66-kv system is in an analogous situation, in that the customers are too numerous for each to be supplied economically with a separate regulator. Instead, the idea of bus regulation is extended, and the entire 66-kv system treated as a sort of bus whose voltage is raised and lowered to fit the load, by using the reactive capacity of generators and of synchronous condensers installed at the load centers and load ratio control at the points of step-down from 220 kv. Since the condensers are required to provide the system reactive capacity, the voltage regulation feature is obtained at little extra cost.

Controlling the transmission system voltage level to suit the load is not an unusual idea, since many companies raise the general voltage level throughout the transmission system as the load increases.

It is the extent to which the practice is followed by this company that is distinctive and requires careful system planning and day-to-day operation.

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N. N. Smeloff is system planning engineer with the Pennsylvania Power and Light Company, Allentown, Pa.

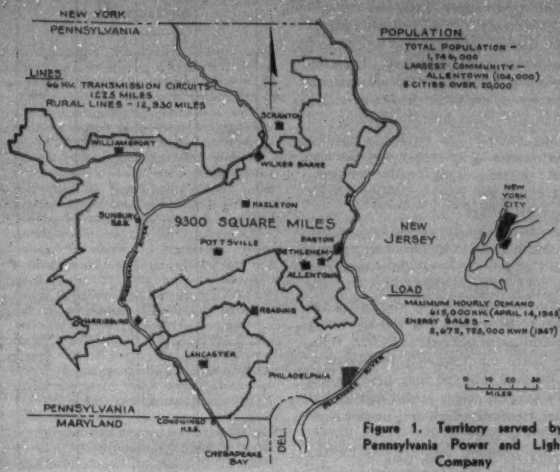

Figure 1. Territory served by Pennsylvania Power and Light Company

Operating Procedure

In practice, the system voltages are controlled by establishing definite voltage schedules based on time for each point where a means of control is available, such as a generator, a synchronous condenser, or load ratio control from the 220 kv. Account is taken in the schedules for the normal variation in system (or area) load from day to day as well as from hour to hour. This company's load has a pronounced morning peak.

A typical schedule for voltage at a control point is shown in Table I. The transition from one voltage to another is made slowly. The operator follows this schedule, within the limits of the equipment under his control, without any further instructions from the central control authority. If the voltage gets beyond the ability of the local equipment to control it, assistance may be obtained from adjacent areas. The voltage schedules have been established and adjusted as experience dictates to give adequate voltage to the customers.

At the present time, there are 32 manually-operated control points, of which 19 are generating stations. In addition to the generators there are at various points around the system slightly more than 125,000 kva of synchronous condensers or other synchronous equipment. It is planned to reduce the manual control points to 22 by 1951 by relocation of facilities, conversion to automatic operation, dispersal into automatically-controlled installations of switched capacitors, and shutting-down of older generating stations.

That such a method of voltage control is quite feasible is borne out by the fact that a system peak load of well over 700,000 kva was carried with less than 60,000 kva of this being served through feeder voltage regulators. Within the next few years it is planned to eliminate half the feeder voltage regulator capacity at locations where the present facilities are of inadequate capacity and must be strengthened. Those to be left are required because of special circumstances as will be discussed later.

In Figure 3 are shown the general types of voltage curves which are obtained at supply points on a normal weekday. Analysis of the voltages of 111 major points of supply on the system shows 29 per cent type A, 37 per cent type B, 30 per cent type C, 4 per cent type D, and no type E curves. Present plans call for elimination of the type D which are not satisfactory for a supply point. Some of the type C curves will be improved on feeders which are so long or heavily loaded that the voltages at the far end are type E due to line drop. Others will be allowed to remain type C until the load growth requires corrective measures.

At the present time, an extensive program of installation of 90,000 kva of switched capacitors well dispersed around the system is under way. These capacitors are to be used for two purposes, voltage control, and release of transmission and distribution capacity. By controlling each capacitor bank with a voltage-measuring relay combined with a weekly time clock, which changes the calibration of the voltmeter to provide higher voltages during peak load periods, aid is given to the system voltage control plan. Capacitors, switched on-and-off by a voltage relay, reduce the on-peak drop and allow more load to be carried without exceeding the allowable voltage variation or spread.

In establishing the new Sunbury generating station, where an initial installation of 150,000 kw is planned for service early in 1949, it was necessary to be sure that the generator would provide the voltage variation required and still maintain stability. The plant will be required to operate at full kilowatt load and with very small reactive loads at off-peak-hour voltage levels. These requirements were met at no additional cost.

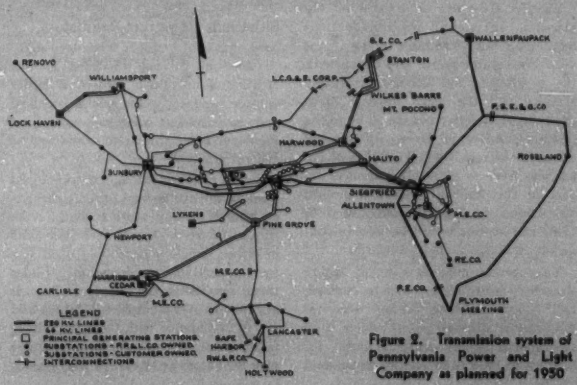

Figure 2. Transmission system of Pennsylvania Power and Light Company as planned for 1950

Table I. Typical Voltage Schedule

Time of Day	Monday and Tuesday, Percent	Wednesday, Thursday, and Friday, Percent	Saturday, Percent	Sunday and Holidays, Percent
Midnight–7 a.m.	100	100	100	100
7 a.m.–11:30 a.m.	104.5	103.8	104.5	100
11:30 a.m.–1 p.m.	102.5	102.5	102.5	100
1 p.m.–4 p.m.	103.8	103.4	103.0	100
4 p.m.–10 p.m.	102.5	102.5	102.5	100.9
10 p.m.–12 p.m.	100	100	100	100

Historical Example

A good illustration of the principles and methods used for controlling voltage without feeder regulators, is afforded by the history of development of a property acquired by the company in 1930. In a region covering nearly 1,200 square miles having a 60-cycle load of 23,000 kw, the only source of power at that time was a single 42,500 kva, 66–12 kv step-down substation approximately in the center. The power was carried away from the substation by a 12-kv subtransmission system with the lines running radially. No customers were served directly from the 66 kv and only in the immediate vicinity of the 66—12 kv substation were there any 12-kv customers. All the other power was distributed at 4,600, 4,000, or 2,300 volts obtained by step-down stations from the 12 kv and controlled by feeder voltage regulators. The 12 kv was unsuitable for serving residential loads or even noncritical industrial loads because of its excessive voltage variation. At some places, variations as high as 17 per cent were obtained and 12 per cent was common. With such voltage drops the losses, both power and reactive, were excessively high.

Shortly after the acquisition of the property, an over-all regional development plan was formulated. This provided for establishing a 66-kv loop with ties to existing and planned generating stations and the main system. Step-down supply points from the 66-kv to the 12-kv system would be set up to serve the areas as required. The 66- and 12-kv lines were to serve all customers except in the built-up areas for general distribution to smaller loads where a 4,000-volt system would be used in conformity with the system standards.

To provide immediate relief before these long-range plans could be realized, two 12-kv switching centers were established at strategic points remote from the original supply center, suitable for later use as 66—12 kv supply centers and where 12-kv lines could be brought together easily. At each of these centers fully automatic 5,000 kva synchronous condensers were installed. This immediately provided additional carrying capacity on the 12-kv circuits by taking advantage of load diversity and improved power factor. The resulting improvement in area 12-kv voltage variation also made it possible to supply industrial, residential, and rural customers directly from this voltage.

Shortly thereafter the 66-kv loop was started and 66—12 kv supply centers provided as and where necessary to unload the 12-kv lines. Load growth in the region was cared for by converting the 4,600-volt lines to 12 kv and shortening the distances served at 4,600 volts. Gradually the 4,600-volt voltage regulators were removed from places where they were no longer needed and used in other locations which still needed their help.

This program was delayed by the war but is now proceeding rapidly. Eventually it is planned that the 4,600-volt lines and regulators will be removed almost entirely and the feeder voltages controlled by the 66-kv system variation supplemented as needed by local switched capacitors and synchronous condensers.

The condition of this property at the time of purchase illustrates in an exaggerated manner a major disadvantage of regulators. The voltage supplied to the 4,600-volt customers was adequate for utilization yet the 12 kv had been allowed to deteriorate until it was unusable to supply loads directly. Thus, even the larger customers had to be served through 12-4.6 kv transformers and regulators. Any load growth meant establishing new substations and 4,600-volt feeders.

Situations Requiring Regulators

There are certain situations, as was mentioned previously, where a feeder voltage regulator is the most economical and satisfactory solution. Several of these occur on this system and are of general interest.

Such a situation is that of a load tapped from a relatively weak circuit such as an interconnection which is used for the transmission of power in varying amounts in either direction depending upon system generation requirements. Under these conditions the supply voltage is apt to vary over a considerable range and not in accordance with the local voltage requirements. The lowest supply voltage may well occur at the heavy load periods

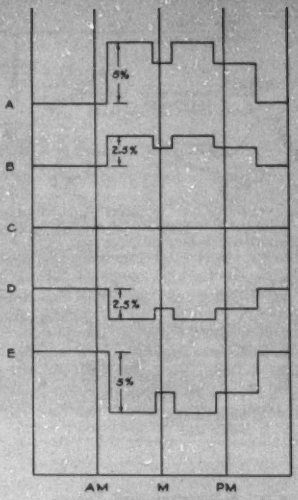

Figure 3. General types of voltage curves at supply points on week days

of the area. The solution for such a situation is most easily provided by a feeder or bus voltage regulator.

A similar problem is posed by a small load fed directly from a generating station low-tension bus. Where the generating station has a high-side voltage schedule to follow, the low-side variation may be too great for the load. To avoid "letting the tail wag the dog" by limiting the generator bus voltage variation to suit the local load at the expense of the main system requirements, a regulator is installed.

Summary

Plans for the future development of the company call for a continuation of operation without regulators as a general practice. By co-ordinated development of the system as a whole, it will be possible to avoid the use of regulators, except as temporary crutches for a few weak spots. Installation of switched capacitors to control voltage on feeders or busses helps not only the local feeders or busses, but also the higher voltage systems supplying them. The cumulative effect of many individual installations is quite appreciable.

As a contrast the feeder voltage regulator controls only the voltage beyond its location and adds its reactive burden to the system supplying it.

Discussion

H. C. George (The Detroit Edison Company, Detroit, Mich.): The Detroit Edison Company like the Pennsylvania Power and Light Company has found it possible to eliminate some line regulators as a result of improvements in power house voltage regulation. There is a difference, however, in the extent to which regulators have been removed and also a difference in the method of varying the power house voltage. Our practice can be described rather briefly.

First, we believe that as a load building proposition it is advisable to maintain good voltage for residential customers. Many appliances are much more satisfactory with close regulation. This has no doubt been a factor in building up our range load to 26 per cent saturation. The quality of service is closely watched in regard to the amount of flicker existing and also to the voltage spread. In the Detroit district only a 7-volt spread in voltage is allowed at the lighting customer's meter while outside of the Detroit district a 10-volt spread is allowed.

These lighting customers and small power customers are served from 4,800-volt distribution circuits some of which have voltage regulators and the rest of which are fed from regulated busses. At one time each circuit was individually regulated. During those years when new equipment was hard to obtain, it was found possible to remove many line regulators by grouping similar circuits together on each of two or more regulated busses. By careful grouping of lines the usual quality of service was maintained for customers.

Lines serving the larger power customers at 24,000 volts and 4,800 volts are not, in general, equipped with voltage regulators. Many 4,800-volt power lines had regulators at one time but after improvements had been made in system regulation at the power houses, most of the power line regulators were removed and used on new distribution circuits.

Our method of power house voltage control accomplishes the same result as the Pennsylvania Power and Light Company's method but in a different manner. We have four steam plants, two in Detroit, one about 60 miles up stream and the other less than 20 miles down river. Nearly all of our generators have manual voltage control, although some are being equipped for automatic regulation. The voltage at the two city plants will run from 118 to 126 volts, while the voltage at the more distant plants will run from about 116 to 132 volts.

At the two Detroit plants the voltage is adjusted to maintain a definite voltage on a compensated voltmeter. At each of these two plants the current for compensation is obtained from the current transformers on the three or four lines feeding one substation. The substation selected in each case has a load cycle which corresponds quite closely in shape to the system load cycle. Thus by an indirect means, the voltage on the transmission system in the city is made to gradually rise and fall in accord with the changes in system load. The amount of compensation used is such that the voltage on most of the city substation 4,800-volt busses remains fairly constant, that is, sufficiently constant for most power customers without the use of substation voltage regulators.

The two out-of-town plants are controlled in a somewhat different manner. The voltage at these plants is adjusted as required to maintain satisfactory voltage at 24-kv and 40-kv stations and substations around the system. Maximum and minimum limits have been established for four 120-kv-24-kv stepdown stations. In general, there is a 4-volt difference between the maximum and minimum limits. One set of limits is given for so called "heavy load periods" and a lower set for "light load periods."

Synchronous condensers and static capacitors spread around the system give additional help in regulating system voltage. Of the synchronous condensers, we have 11 running from 1,500-kva to 30,000-kva, mostly equipped for automatic regulation. Thirty-one substations have installations of static capacitors which are controlled from time switches.

C. J. Lake (General Electric Company, Schenectady, N. Y.): Controlling the transmission system voltage level to suit the load is not an unusual idea, but the extent of this is limited and if the effective voltage spread is not sufficiently narrow, adequate voltage for all customers will not be available. Therefore, the success of voltage control on a system without regulators depends upon the voltage spread. The more money invested into the system for copper, capacitors, or synchronous condensers, the more the annual carrying charge will be. In return, there will be a saving due to reduced I^2R losses.

The use of capacitors and synchronous condensers to control the reactive power and power factor is basically and economically sound. Maximum efficiency in transmission will be obtained when the power factor is unity and no reactive power flows around the system. A company can afford to approach this criterion in many cases. Hence, any system which cannot maintain adequate voltage limits throughout the entire system after the criterion has been reached must either use regulators or rebuild the lines. An economic study of each particular feeder is the most logical method to assure that the proper choice is made. Naturally, an accurate prediction of the future conditions and changes is never attainable but the better the prediction, the better will be the conclusions of an economic study.

The investment required to change over from manually operated control points to automatic operation is another item that must be considered. Regulators automatically maintain the voltage of electric circuits within predetermined limits and their prices include the control equipment for automatic operation. In other words, some of the money invested in regulators buys automatic control for the system.

As loads grow, there will be a time reached when the voltage spread will have to be reduced even after the maximum permissible amount of reactive power corrective equipment is installed. It may be, very likely at that time, more preferable to use regulators than to rebuild the lines. Another factor vital for the success of system voltage control is the pattern of the daily load curve of every load area in the system. The diversity in the load curves for all parts of this system is very small. In some systems, this cannot be realized due to variety of customer load requirements.

The reactive burden of regulators has many times been improperly interpreted. Actually, the average reactive power needed by regulators in ordinary systems amounts to about 2 per cent of the total inductive reactive power of the load connected to them. The probable error in varmeter readings is about 1 per cent, which is customarily considered insignificant, and it is reasonable to assume that the reactive power needed by regulators is negligible. The increase in system voltages during peak loads usually increases the system inductive reactive power more than this due to the saturation effects in transformers and motors. That is, a 5 per cent increase in the system voltage increases the system inductive reactive power about 10 per cent.

S. B. Farnham (General Electric Company, Schenectady, N. Y.): As electric system loads continue to grow, the economic necessity for full utilization of the existing system tends increasingly to dictate a more uniform voltage level from generating station to point of supply to the load area. This follows from two fundamental considerations. First, the limitation in the loading of such equipment as generators and transformers is generally a thermal one. Hence, if this equipment is to be utilized to the fullest extent for its primary purpose of generating or transforming kilowatts, it must be relieved from carrying kilovars. By generating the kilovars close to the loads where they are needed, as with synchronous condensers or static capacitors, this end is achieved, and at the same time the improved power factor results in reduced voltage drop and consequently a more uniform voltage level from generator to load. Second, the permissible loading of overhead transmission and distribution lines is generally limited by voltage drop. In most cases, the major portion of the drop is that resulting from transmitting kilovars. Again, therefore, by generating these kilovars as closely as possible to the load, the line loading limit is increased and the system voltage gradient reduced.

Smeloff's paper is a timely illustration of a practical evaluation of this situation at the transmission and subtransmission level. He may justifiably take credit for realizing significant gains in uniformity of system voltage levels, while at the same time permitting increased utilization of the kilowatt-carrying ability of his system.

As his projected plan for increasing use of automatically switched capacitors develops, it would be of interest to know what changes, if any, are anticipated in the voltage schedules maintained at generating stations.

W. E. Birchard (General Electric Company, Pittsfield, Mass.): The Pennsylvania Power and Light Company system seems somewhat unusual in that it has many large customers supplied at 66 kv. Presumably, these customers have their own distribution systems, and it would be interesting to learn what spread in utilization voltages they experience. They can, of course, re-regulate the voltage on portions of their systems just as many power companies do.

Another question may be of concern to operating engineers. What restrictions does this method of operation impose on interconnection with other power systems?

Smeloff has stated that his objective is to provide adequate voltage control at minimum over-all cost. But has he thoroughly investigated the relative costs of various combinations of generator control, synchronous condensers, capacitors, and feeder voltage regulators? Eliminating the cost of feeder voltage regulators may have resulted in greater expenditures for lines and other apparatus. Studies of other systems have shown that each item has its proper function, and that feeder voltage regulators, far from being "temporary crutches" or luxuries, are providing the lowest cost means of voltage control when applied in the combination most suitable for that system. There are many additional situations where it is impractical to maintain balanced loading of phases, and balanced loading and length of feeders, and in those cases feeder voltage regulators are a virtual necessity. Regulators are very flexible in that they can be relocated easily to suit the changing requirements imposed by growing and shifting loads.

Smeloff substantiates his conclusions by the statement that 700,000 kva was supplied with only 60,000 kva served through feeder regulators. Are not the following the two real criteria?

1. What were the extremes of utilization voltages actually supplied? (not the schedules of substation voltages that operators were striving to maintain by manual control.)

2. How did system investment and operating costs compare with corresponding costs for a system design utilizing the best combination of feeder voltage regulators with other apparatus such as generator control, synchronous condensers, and capacitors?

D. K. Blake (General Electric Company, Schenectady, N. Y.): We may differ among ourselves as to what voltage spread constitutes good voltage. We do not differ, however, as to where the point of good voltage must be and that point is the consumer.

Smeloff's paper presents a practice that is understandable and agreeable up to the subtransmission supply of distribution substations. But, from the distribution substation bus to the first transformer and the effect the method described by Smeloff has on the allowable drop from the first transformer to the last transformer is not clear. Does Smeloff mean that he regulates the 4-kv bus of each distribution substation with synchronous condensers or switched capacitors, as is commonly done when tapchangers or regulators control the bus voltage to a group of feeders? If he does mean this, then does he locate distribution substations so that the voltage difference from the transformer nearest the substation bus to the transformer farthest from the substation bus regardless of which feeders they may be on, is the same as he would otherwise have on a feeder with individual regulators if they were used. If so, then the point to be cleared up is, how is it possible to do this economically on a system-wide basis, in spite of wide differences in load density and unavoidable long branches?

The higher the load density, the shorter the feeders become and hence it becomes more practical and economical to use group or bus regulation because the difference in length between the bus and first transformer becomes smaller. At the lighter densities, distances become greater, and hence the need for individual feeder regulators in some locations or smaller bus regulated substations in other locations where subtransmission supply is available at reasonable cost. Over and over again, company after company has found supplementary regulation in the form of branch regulators very economical. In fact, supplementary branch regulators make bus regulation practical in many instances.

As far as my experience goes, there is a proper place in the industry for all of the regulating methods. It is quite common to find large systems making use of all of them. Bus regulation, feeder regulation, and branch regulation, have an economic place in distribution systems in addition to capacitor switching, synchronous condensers and generator fields at transmission and subtransmission levels.

The most fundamental of all distribution system regulating problems is the drop between the first and last transformer which determines the most economical feeder size and as a result of this the most economical substation size.

N. N. Smeloff: There are a few additional advantages that capacitors have over voltage regulators which should be mentioned. Voltage regulators raise the voltage along an entire feeder by the same percentage, which results in the first customer placing a top limit on the amount of load which can be carried on a feeder. The use of automatically switched capacitors at the ends of the feeders, on the other hand, raises the voltage level at the end by a greater percentage than at the substation, allowing the maximum load carried to be increased to a higher value, perhaps to the thermal limit of the conductor in some cases. To answer the question raised by Blake, the capacitors for this purpose are normally located some distance out along the feeder and not at the supply point bus.

In general, the use of feeder voltage regulators results in the deterioration of the voltage of the supply system, such as the subtransmission, to the point where it cannot be used to serve even industrial customers directly without additional regulators. In the long run, this tends to produce a situation where all power used passes through regulators with a resulting increase in system investment.

We are pleased to hear from George that the Detroit Edison Company has found it possible to eliminate some line regulators by using powerhouse voltage control instead, because it confirms the results of our studies and practices. We agree that it is desirable to maintain good voltages for residential customers, and our standards for service to this class of customer are about the same as those described by George. For industrial customers, we allow a somewhat larger variation.

Using Lake's figures for the reactive burden of regulators, the additional load to be supplied by the Pennsylvania Power and Light Company would be approximately 15,000 reactive kilovolt-amperes. While this is not an extreme amount, it is a load which we do not have at present.

The increase in system load with increased powerhouse voltage is present whether or not regulators are used, so long as the system voltage is increased during peak load periods.

In answer to Farnham's question, it is not anticipated that there will be any major changes in the voltage schedules of the generating stations as automatically switched capacitors are added to the system, because they are added only to compensate for increased drop between the generating station busses and the loads.

Judging by the first paragraph of Birchard's discussion, the paper does not make clear enough the relation between the voltages supplied to the customers and their needs. It is our practice to furnish voltages adequate for general distribution to residential and commercial loads over relatively long distances compared to any encountered by individual customers. To the best of our knowledge, no customer regulates the voltage supplied him except for very special types of loads requiring an exceedingly fine degree of control such as laboratories. In this case a small regulator is used on only that small portion of the load requiring the close control.

There are practically no restrictions imposed upon interconnections with other companies, because most utilities raise and lower their voltages in a manner similar to the Pennsylvania Power and Light Company. As was pointed out in the paper, the method is not uncommon; it is only the extent to which it is carried that is not customary.

With regard to the economic question raised by Birchard, studies made in the past indicated that for a far-flung network such as the Pennsylvania Power and Light Company, with customers and generating plants at so many locations, it is cheaper to limit the supply point voltage variations so that regulators are not needed. We agree that the engineering required for application of this method is more difficult, but an economical solution can usually be obtained.

With regard to the criteria for the adequacy of voltages supplied without feeder regulators:

1. The number of voltage complaints received from customers is not excessive.

2. There is a trend in many companies for the elimination of feeder voltage regulators at the present time, particularly for high-density areas.

3. The number of kilovolt-amperes supplied without passing through regulators is offered only as supporting data that such a method is indeed feasible.

Planning the Development of a Metropolitan Electric System

M. L. WARING
MEMBER AIEE

THE Consolidated Edison System supplies electricity to an area of 535 square miles including the major portions of the city of New York and of Westchester county which joins the northern boundary of the city. This large metropolitan area contains a population of over 8,500,000 people with a load density varying from a maximum of 220,000 kw per square mile in the highly concentrated areas, to a minimum of only about 100 kw per square mile in the suburban areas.

The electric load consists mainly of services to small customers, of whom about 2,500,000 are supplied directly, with possibly another 400,000 served indirectly. Aside from the supply to railroads and rapid transit lines, large industrial loads represent less than four per cent of the total load. It is probable that two-thirds of the peak demand is used for lighting purposes. Estimates have been made which indicate that New York city contains the largest concentration of small business in the country with some 230,000 concerns, each employing less than seven workers on the average. The effect of these factors has been to decrease the fluctuations in load caused by business cycles as compared with other systems having a larger proportion of heavy industrial load.

During the past 20 years, the peak load supplied to the area has more than doubled. Served at the beginning of this period by a number of individual operating companies, the system is now completely integrated and operates all major power stations in the area with the exception of three plants owned and operated by the Board of Transportation of the city of New York. The integration of facilities together with a large increase in load have resulted in major changes to the system. The development of the system during these 20 years has been accomplished as a gradual process guided by the following general fundamental policies which were formulated early in the period and have been adhered to up to the present time:

1. To maintain a margin of reserve or spare capacity determined by the probability method and designed to provide a minimum but adequate degree of reliability.

2. To expand and modernize existing low-pressure generating stations by the installation of topping turbines and associated high-pressure boilers.

3. To reinforce the tie feeder system interconnecting generating stations when required to minimize the over-all system reserve capacity, but to install topping turbines, wherever practicable, as the first means for providing additional capacity needed by local areas or parts of the system.

4. To segregate the secondary network system into isolated areas each fed independently from a single switch gallery and limited in size to a load that can be reenergized in the event of a shut down.

It is the purpose of this paper to review these policies and to summarize their effect on the design of the system.

Determination of Generating Capacity Requirements

Shown in Figure 1 are curves illustrating the relation between the system net generating capacity and 1-hour peak loads for each of the years from 1920 to date. Due to the integration of a number of individual operating companies and the effect of the depression, ample reserve capacity was available for a number of years. However, the abnormally high rate of growth of the post war load has made it important to determine accurately the amount of reserve generating capacity required for contingencies and for overhaul. The system contingency reserve requirements have been determined by means of the probability method, using for this purpose the following basic information.

Unit Forced Outage Rate. This factor represents the per cent of the operating time that any unit of equipment may be expected to be forced out of service due to contingency. Based on national average data modified to reflect the Edison System experience, the following rates were used: high pressure boilers, 3 per cent; low pressure boilers, $2^1/_2$ per cent; high pressure turbines, 3 per cent; low pressure turbines, $1^1/_2$ per cent.

Variation of Daily Maximum Capacity Requirements for Load and Overhaul. In a calendar year there are 253 days excluding Saturdays, Sundays, and holidays which must be considered in an analysis of the probability of forced outages in excess of available reserve. The rapid growth of summer load in recent years has steadily narrowed the margin of capacity available to schedule the outage of equipment for overhaul during the off-peak months. Since this trend is expected to continue, it is necessary to plan on the assumption that the capacity needed during the summer months to meet the peak-hour load plus scheduled overhaul requirements will be substantially the same as the capacity required to supply the winter peak-hour load. However, there is a variation in peak load from day to day due to weather conditions. During the summer months this variation (excluding short thunderstorm peaks) exceeds 200,000 kw, and in the winter it amounts to about 100,000 kw. The daily variation appreciably decreases the exposure to forced outages in excess of available reserve. A correction factor of 90 per cent was introduced, therefore,

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M. L. WARING is a planning engineer in the system engineering department of the Consolidated Edison Company of New York, Inc., New York, N. Y.

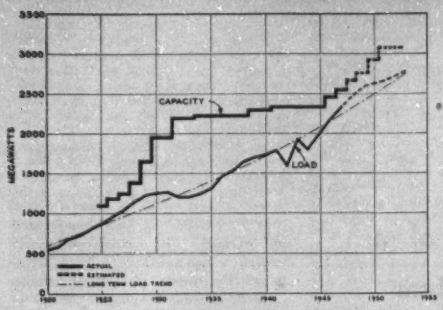


Figure 1 (left). Comparison of net generating capacity with net maximum one-hour load for Consolidated Edison System

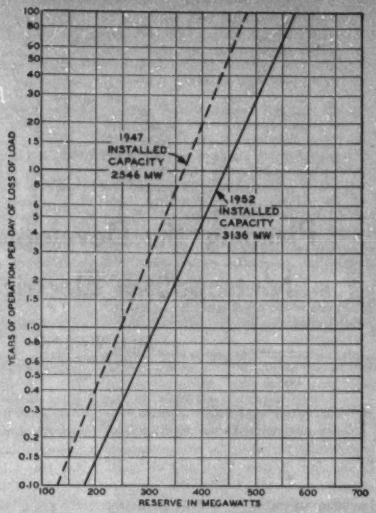


Figure 2. Years of operation per day of loss of load as a function of reserve in megawatts

into the calculations, this being representative for the system. This correction factor is the ratio of the average requirements for load and overhaul to the peak requirements.

Type of Contingencies. The probability method used to determine reserve requirements considers unit outages as random events obeying the binomial law of probability. The method does not take into consideration protection against major catastrophies which might involve the outage of an entire station. Furthermore, the calculations are limited to the outages of turbines and boilers alone. With the major tie reinforcements now authorized and under construction, the studies indicate very definitely that system reserves can be made available to any portion of the system in adequate amounts, and that the only value needed for planning purposes is the reserve requirement for the entire system.

The Edison System is interconnected with adjoining power systems. Such interconnections decrease the amount of reserve capacity required as compared to that needed if each system were to operate independently, inasmuch as it is unlikely that adjoining systems will be subjected to extreme contingencies simultaneously. Although no accurate determination has been made, approximate studies indicate that the Edison System, in an emergency, could rely on at least 80,000 kw of reserve capacity from its interconnections if adjoining systems maintain reserves computed in a similar manner.

Various means of reducing load are available which do not inconvenience consumers too seriously. Chief among these is voltage reduction which, if limited to about 5 per cent, will produce a like reduction in load without undue hardship. In the case of the Edison System, it is practicable to reduce load to the extent of 150,000 kw in the winter and 100,000 kw in the summer on very short notice.

Summarized in Figure 2 are the results of reserve calculations for the system as it exists today and as it will exist as of 1952 after completion of the various units now under construction.

The determination of the desirable reserve capacity to be maintained is a matter of judgment in which it is necessary to balance the cost of capacity additions against the degree of reliability desired. Present plans are based on maintaining a minimum installed generating reserve capacity of 350,000 kw, which when added to the 80,000 kw of interconnection capacity would provide a total reserve of 430,000 kw. This will provide a spare capacity of 16 per cent of the 1952 peak hour load. Capacity outages in excess of this amount can be expected to occur on the Edison System approximately one day in eight years. Should such excessive contingencies develop it will then be possible to resort to voltage reduction, and it may be seen from the curves in Figure 2 that it would not be necessary to exceed a load reduction of 100,000 kw more frequently than one day in about 50 years. Because of the inherent stability of the Edison System, reasonably accurate forecasts of load can be made for periods up to four or five years in advance and a close co-ordination between load

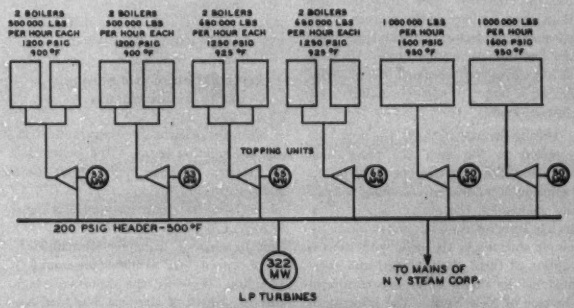


Figure 3. Schematic diagram of boiler and turbine equipment at Waterside Station after completion of present construction program in 1949

growth and new capacity installations can be maintained.

Expansion and Modernization of Existing Plants by the Installation of Topping Turbines

It is apparent from an inspection of Figure 1 that the cessation of load growth during the depression years made it unnecessary for a number of years to provide additional generating capacity to meet the total system load requirements. At that time the system generating plants operated at pressures ranging from 200 to 400 pounds per square inch gauge. The most efficient plant had a heat rate of approximately 15,000 Btu per kilowatt-hour while the 200 pounds per square inch gauge stations had heat rates of about 25,000 Btu per kilowatt-hour.

The Waterside station was then a 200 pound per square inch gauge plant containing a number of 25-cycle condensing turbine generators supplied by a large number of small boilers. The station is located strategically in Manhattan adjacent to the East River, directly opposite the heavily congested Grand Central and Times Square areas. Due to the rapid growth of the 60-cycle a-c system caused by the changeover of many consumers from d-c to a-c supply, it became imperative to increase greatly the 60-cycle supply to this midtown area.

Studies, which included the evaluation of relative operating expenses as well as annual carrying charges on capital costs, showed substantial savings in favor of providing the capacity required for the midtown Manhattan area by installing topping turbines at Waterside, rather than by transporting large blocks of power over additional tie lines from adjoining stations. A total of four 60-cycle topping turbines exhausting steam to the 200 pounds per square inch gauge header were installed prior to the war for this purpose. High-pressure and temperature boilers having an efficiency approaching 90 per cent replaced the older, low-pressure boilers which had an efficiency of only about 75 per cent. The efficiency of steam utilization in the existing low-pressure turbines remained unchanged, but the new topping units produced additional electric energy at a heat rate of about 4,500 Btu per kilowatt-hour.

Two additional topping units, associated boilers and two new 200 pounds per square inch gauge condensing turbine generators are now under construction at Waterside. A diagram of the station as it will exist after completion in 1949 is shown in Figure 3. There will be a combined generator rating of 658,200 kw, which is an increase of 80 per cent over the 1934 station capacity of 366,200 kw. Furthermore, the heat rate of the station will have been improved from some 25,000 Btu per kilowatt-hour to better than 11,000 Btu per kilowatt-hour. In this manner it was possible to more than double the over-all station efficiency and to decrease substantially the unit operating labor and maintenance expenses of the system.

In the case of the Waterside development, retention of the 200 pounds per square inch gauge steam header offered two unique advantages. The first of these is to permit the gradual conversion of the existing 25-cycle turbine generator units to 60 cycles, as progressive changeover from the d-c to the a-c supply system may develop. The second advantage is that it facilitates obtaining substantial production savings in the supply of steam to the district steam heating customers of the New York Steam Corporation. The uptown district of the Steam Corporation is supplied in part from the 200 pounds per square inch gauge header at Waterside. The exhaust steam from the topping turbines can be used, therefore, either for the production of electricity in the condensing turbine generators or to supplement the supply of steam to the district steam system. Because of diversity in time between the demands of the electric and steam systems and by pooling of reserve capacity requirements, substantial savings in capacity and production costs have been obtained. Sufficient feedwater treatment equipment has been provided to permit the supply of as much as 2,000,000 pounds per hour of steam to the district. This method of operation has proved itself to be practical and economical.

As shown in Figure 4, two topping turbines and boilers have been installed in the Sherman Creek station. All but two of the low-pressure machines were 60 cycle and these two have since been rebuilt from 25 to 60 cycle operation. The capacity of the station has been increased from 151,000 kw to 271,000 kw and the heat rate of the modernized portion of the station has been improved from about 24,000 to less than 12,000 Btu per kilowatt-hour. This development provided the capacity needed to meet the growth in load of the northern area served by the Edison System.

The third station to be modernized by the installation of a topping unit was the Hell Gate plant, located in the south portion of the Bronx on Long Island Sound. As one of the base load stations of the System, Hell Gate had been designed originally to accommodate the type of daily load cycle then prevalent. As a result, the boiler capacity was sufficient to supply the full load steam requirements of the turbine generators for a few hours over the peak load period of the day. However, because of a very marked increase in daily load factor, the boiler

Figure 4. Schematic diagram of boiler and turbine equipment at Sherman Creek Station

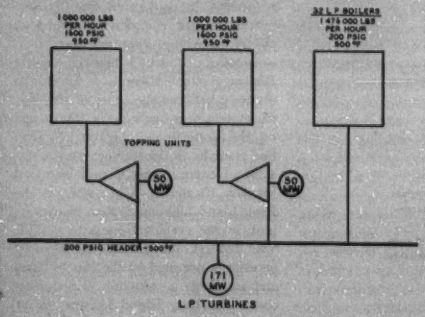
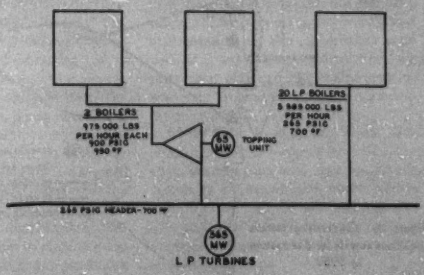


Figure 5. Schematic diagram of boiler and turbine equipment at Hell Gate Station



plant was no longer capable of supplying the sustained day load requirements caused by the change in load characteristic of the system. To correct this situation additional boiler capacity was required and it was found more economical to install the needed boiler capacity at higher pressures and temperatures with a topping turbine exhausting steam to the existing 265 pounds per square inch gauge steam header. Very little increase in peak hour station capacity was obtained since it was desirable to remove a 40,000-kw turbo-generator as well as two rows of boilers to provide room for the new installation. This development differs, therefore, from those at Waterside and Sherman Creek in that the primary purpose was to increase the sustained load capability of the plant. A diagram of the completed station is shown in Figure 5.

Plans are now being prepared to install a topping turbine at the Hudson Avenue plant for reasons very similar to those already explained for the Hell Gate station. This will be the tenth topping turbine to be added to the system and will bring the total capacity of such units to 556,000 kw. It will practically complete the Consolidated Edison's program of modernizing and expanding its low-pressure plants by the installation of topping turbines. All 200 pounds per square inch gauge boilers have been retired with the exception of approximately 200,000 kw of such capacity. This closely approximates the standby or peak load capacity required for the Edison System and it is unlikely that there will be economic justification for retiring additional 200 pounds per square inch gauge boilers in the near future. All other capacity installations contemplated or under construction at this time are of the full expansion type.

Interconnections Between Generating Stations and With Other Systems

The expansion of existing generating stations by the installation of topping units has been the most economical means for providing additional capacity required to accommodate load growth in local areas or parts of the system. Now that the topping program approaches completion it has become increasingly desirable to reinforce the tie feeder system. Accordingly, the installation of large capacity ties between generating stations is being accelerated.

Tie feeders between generating stations and between independent systems are installed, generally, for one or all of the following reasons:

1. To pool reserve capacity.
2. To permit interchange of energy for best over-all production economy.
3. To take advantage of diversity in time of area or system peak loads.
4. To provide transmission capacity to distribution substations located at load centers.

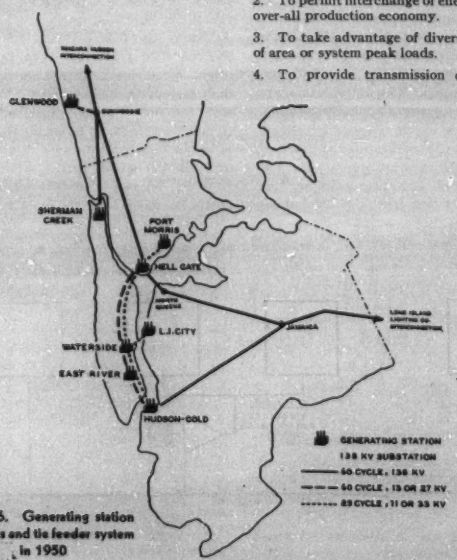


Figure 6. Generating station locations and tie feeder system in 1950

Shown in Figure 6 is a map of the Edison System after completion of work in progress giving the geographic location of the generating stations, the tie feeders interconnecting them, and the two interconnections with adjoining systems.

The interconnection with the Niagara Hudson System is a double circuit, 138,000-volt overhead transmission line extending from the Dunwoodie substation in Westchester County to the Pleasant Valley substation of the Niagara Hudson System located near Poughkeepsie. Placed in service in 1932 it has proved itself to be useful for most of the reasons given previously.

As explained in a preceding section, the benefits obtained from the pooling of spare or reserve generating capacity requirements through interconnections amount to a capacity saving on the Edison System of at least 80,000. A major share of this saving is obtained from the Niagara Hudson tie. In addition it has provided an outlet during high water periods for energy produced upstate by hydro stations at such times as there was an insufficient amount of load on the Niagara Hudson System to absorb such power. It has permitted the Niagara Hudson System to purchase energy produced in Edison's steam plants at times when the cost of production in steam plants on the Niagara Hudson System would have been higher and vice versa. The tie line has served as a means for transmitting power to a total of three stepdown distributing substations supplying the bulk of the electric load in Westchester county.

The second interconnection, placed in service recently, ties the Edison System with that of the Long Island Lighting Company. This tie contributes to the total savings in installed capacity requirements and will permit substantial reductions in production cost by permitting either company to purchase energy from the other whenever it is economical to do so. Unlike the Niagara Hudson System, there is a substantial diversity between the times of the electric system peak loads on the Edison and on the Long Island Lighting System. For example, in the summer months the Edison System reaches its peak hour load between 10 and 11 A.M., whereas the Long Island Lighting System reaches its peak in the evening from 8 to 9 P.M. This reduces the amount of capacity necessarily operated on the two systems, and so effects a reduction in operating cost. The Long Island Lighting tie at

present consists of a single circuit 138,000-volt underground cable. However, facilities have been provided to install a second circuit at some time in the future. The tie extends from the Jamaica substation to the Valley Stream substation of the Long Island Lighting Company.

Work is now in progress to extend the 138,000-volt tie line from the Jamaica substation to the Hell Gate generating station. Upon completion there will be a double circuit tie line from Hudson Avenue to Jamaica and from Jamaica to Hell Gate. Completion of this work will form a tie loop between Hell Gate and Hudson Avenue in parallel with the lower voltage ties running through Manhattan. These latter ties operate at either 27,000, 13,000, or 11,000 volts and tie the Waterside and East River stations into the loop between Hell Gate and Hudson Avenue. To provide adequate control of power for this extensive loop both phase angle and voltage control apparatus are being installed as shown in Figure 7.

The tie feeder additions described before are scheduled for completion in 1950, and at that time there will be sufficient tie capacity between the various generating stations on the system to permit a complete pooling of reserve capacity requirements for the individual portions of the Edison System. Meeting this criterion provides more than enough tie line capacity to make it possible to select and operate individual generating units in their most economical combination for any level of system load.

The 138,000-volt tie loop between Hudson Avenue and Hell Gate will also serve as transmission to the Jamaica and North Queens substations. These substations will reduce the voltage to 27,000 volts and will supply substantially all of the load distributed in the Borough of Queens.

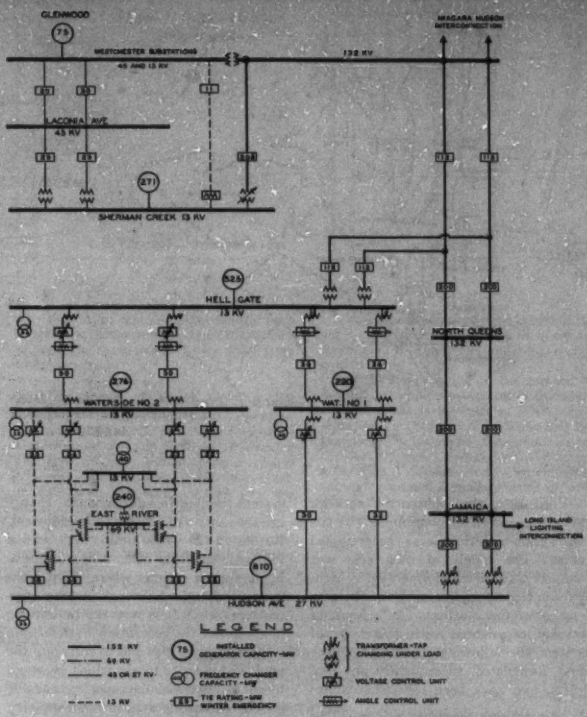


Figure 7. 60-cycle system diagram in 1951 showing phase-angle and voltage control

The Transmission and Distribution System

A number of changes have been made in methods used to distribute electricity which have produced corresponding changes in the fundamental basis of the plan adopted for the Edison System. The supply to Manhattan was developed originally as a d-c distribution system and because of the requirement for uninterrupted service and the importance of its load the d-c network system continued to be developed until 1931. The northwest section of Brooklyn was also developed originally with a d-c system. As the electric art developed, a-c systems of distribution came into existence. The Boroughs of the Bronx, Brooklyn, and Queens were developed with a-c systems of distribution utilizing substations as distributing stations from which emanated 2-phase primary feeders supplying 3-wire single phase secondaries. With the continued development of new equipment and increased load requirements, Brooklyn, Queens, and the East Bronx changed their 2-phase primary distribution systems to 3-phase systems. The West Bronx because of its large 2-phase primary commitments continued to develop the 2-phase system. The generation and transmission of electricity to the d-c substations was 3-phase alternating current at 25 cycles when supplying the d-c secondary system and 60 cycles for a-c substations supplying the a-c secondary system. The development of the a-c network protector in 1926 caused a radical change in a-c distribution. This device made it possible to eliminate the need for a-c substations and to provide an a-c service with a reliability similar to that of the d-c system.

Increased operating experience and improvements in the a-c network unit, protector and transformer, have resulted in establishing the 3-phase 120/208 volt a-c secondary network system as the standard throughout the system territories. The company requires that new load be arranged for a-c service and, as a result, the a-c network gains in area and load density as the opportunity and necessity arise. In addition the a-c network is extended into radial districts whenever the load density and other conditions warrant.

In the lower load density areas, such as are found in the outlying sections of the city, the company still uses the radial system of a-c distribution which affords customers an entirely adequate service. The cost of this service in such areas is appreciably lower than that of an underground a-c network.

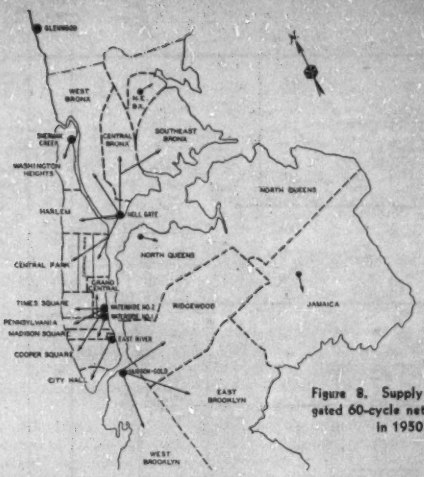


Figure 8. Supply to segregated 60-cycle network areas in 1950

Until 1936, the 3-phase a-c network system consisted of continuous 120/208 volt grids covering large geographical areas. The supply of each grid was through network transformers fed by high-tension feeders emanating from the several generating stations. As a result of trouble experienced with this type of system the following guiding principles were formulated, subsequently put into effect, and have been adhered to since:

1. A-c network secondary grids shall be subdivided into network areas, each area being completely segregated from all other network areas. The load in each area shall be small enough so that the area can be reenergized independently by the station which serves as its base supply.

2. Network feeders shall carry load to only one network area and shall not be bifurcated with feeders to other areas.

3. Each network area shall be fed from a single generating station.

4. The supply to each area shall be distributed among sufficient bus sections to permit the loss of any one major bus section.

The adoption of this type of network system resulted in a fundamental change in the plan of system development. Rather than intermesh high-tension feeders from all the generating stations as in the former manner where the stations were synchronized through common secondary grids. It is now the practice to interconnect the generating stations by a system of direct ties and to segregate the network load into blocks which are small by comparison with the capacity available on the generating station bus supplying them. In addition to simplifying the normal and emergency supply to distribution load, this method offers great flexibility in the planning of the expansion of the system. It is possible now to relieve any portion of the system needing load relief either by transferring load away from it to some other part of the system, or by reinforcing the direct tie feeders connecting it with the balance of the system.

Until very recently it has been the practice to install 13- or 27-kv distribution busses and switching equipment as an integral part of the generating station and distribute directly to the load from the station except in outlying areas such as Westchester county. The cost of such conventional switch gallery construction in New York city has reached a point where it is no longer economical to distribute directly from the generating stations. Estimates indicate that so long as present costs prevail, it is more economical to construct these switch galleries at a site where horizontal rather than vertical construction can be carried out. As a result, under present conditions, it is considerably less costly to install double transformation and construct the switching facilities at a location where the equipment can be spread horizontally even though the substation is located within a very short distance of the generating station. The first of these network area distributing stations is now under construction and will be located approximately two miles from the East River generating station. In the East River development it was found that due to space limitations it would be necessary to construct the gallery vertically if located at the station. Coupled with this condition was the fact that the system has grown to a point where circuit breaker duties have become a serious factor. Accordingly, a 69-kv bus will be erected at East River to which will be connected, through transformers, the generators and the incoming ties from the adjacent generation stations. 69-kv underground feeders will be fed from this bus and they will supply the network area distributing stations.

In Figure 8 are shown segregated network areas and the distributing busses from which they are supplied. Of particular interest are the supply to the City Hall network area from the East River station as described before and the supply to the North Queens and the Jamaica substations from the new 138,000-volt tie feeders between Hell Gate and Hudson Avenue. These three area substations represent recent and marked changes in the fundamental plan of the transmission system.

Discussion

C. N. Metcalf (Consolidated Edison Company of New York, Inc., New York, N. Y.): Waring has condensed an immense amount of detailed planning for the Consolidated Edison System over many years into this paper and he should be commended for the clarity of presentation.

It has been suggested that a brief discussion of the method of supplying the secondary network grids or areas in the Consolidated Edison System would be of interest in connection with this paper. As stated in the paper, "the secondary network grids are subdivided into areas—completely segregated."

Figure 1 of the discussion shows how each of the separate secondary network areas is supplied from segregated major bus sections divided physically by fire walls to prevent the spread of trouble. Six major bus sections are shown, a minimum of four sections has been established as basic design. Each of the major bus sections is subdivided into two minor sections by a circuit breaker and thus one-half of any major bus section may be restored in a relatively short time after failure.

Twelve distribution feeders are grouped in two feeder bands—1 and 2, 3 and 4, and so forth. The two feeders comprising each band are supplied from different major bus sections. Thus, in the event of failure of one of these major bus sections, both feeders

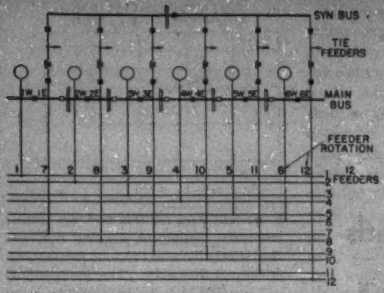


Figure 1 (left). Network supply plan

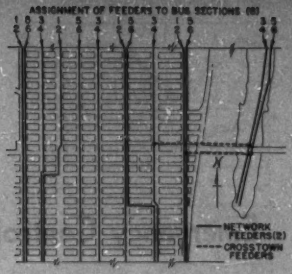


Figure 2. Master feeder plan

will not be out in any of the bands. The distribution feeder supply to the network grid is designed to cover simultaneous outage of any two feeders without serious contingency conditions.

A typical master feeder plan, with assignment of feeders to the six major bus sections, is shown in Figure 2 of the discussion. This indicates clearly how the distribution feeders are run in pairs or "two feeder" bands, physically separated from other feeder bands in the network grid and in routing to the network grid area.

The sizes of network areas, are important from a design standpoint. Each area must be small enough so that it can be energized independently. The maximum size is dependent on the number of feeders that can be closed simultaneously or in groups, in quick enough succession, so that network protector fuses will not be blown by short time overloads.

The maximum size of a network area is also dependent on the ability of the boiler plant and tie feeders to absorb the load imposed on re-energizing. Since blocks of load may be picked up in progressively larger amounts, the sizes of the areas may vary. For the Consolidated Edison System, the load of the areas varies from 40 to 75 megawatts where they are supplied by 13,000-volt distribution feeders and up to 150 megawatts where supplied by 27,000-volt feeders. These limits are not necessarily the maximum as this is reviewed as conditions require.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper is of considerable value to the industry. I have a number of questions as follows:

1. Referring to Figure 1 and the discussion in the paper, are the data on "net generating capacity" based on the nameplate rating of the generators or on what?
2. In what manner is the possible error in load forecast covered? That is, is there an allowance made for such an error by adding to the initially forecast load or is the allowance intentionally covered by the magnitude of the reserve that is determined to be required?
3. In Figure 7, are the emergency winter ratings, for example, for the two 200 megawatt tie lines between Jamaica and North Queens based on both lines being in service at the same time and carrying such a load? (This question is asked because I think that both these lines are in the same trench.)
4. What is an outline of the method used to arrive at the figure of "at least 80,000 kw" as being the reserve capacity available from interconnections?

E. L. Michelson (Commonwealth Edison Company, Chicago, Ill.): Waring's paper is an excellent summary of the history of the Consolidated Edison System. Papers of this type are very helpful to other system planning engineers, since it aids in evaluating situations that occur on their own systems.

It may be of interest to point out some of the situations on the Chicago System that parallel the conditions outlined by Waring in his paper.

In Chicago it became necessary to establish heavy interconnections between generating stations approximately 20 years ago. The availability of water and other factors, did not permit the installation of capacity at the load centers, and, therefore, it was necessary to install generating capacity somewhat remote from the load and to transmit the power over 66-kv cables to the load centers.

The Consolidated Edison System has made extensive use of topping units to modernize their system, and to provide additional generating capacity. In Chicago this has been done to only a limited extent—two topping units of a total capacity of 80 megawatts having been installed in recent years.

In Chicago, as in New York, the use of the a-c network system has been adopted for power sypply to the downtown area to replace the d-c system. The principle of developing the network in small sections is being followed in Chicago in a manner similar to that described by Waring.

It was interesting to note that in New York the practice of distributing power at the generated voltage is being replaced by the use of 69-kv feeders. In Chicago this plan was developed a few years ago, and there are now several 69-kv substations under construction. These substations eliminate costly construction at the generating stations due to the need for high-capacity equipment at these locations, they eliminate the use of long 12-kv lines to substations, and they permit expansion of switching structures more nearly in proportion to the system load growth.

M. L. Waring: The author is indebted to the discussors for describing in greater detail some of the points covered briefly in the paper and for indicating the similarity of conditions that exist in different metropolitan areas.

Metcalf's description of the method of supplying completely segregated secondary network grids is interesting. This represents the most satisfactory solution that has been found to overcome the difficulties encountered when feeders from different generating stations were synchronized through common secondary grids of wide extent.

Halperin raised a number of pertinent questions to which the answers are as follows:

1. The "net generating capacity" of the production plants is their net peak or 1-hour capability after allowance for variations from the nameplate rating of the equipment as determined by operating experience. In some instances the plant capacity is limited by the capability of the boilers after an allowance for the normal outage of stoker-fired boilers as required for cleaning purposes.
2. No allowance for possible error in load forecast has been made. As stated in the paper, due to the inherent stability of the Edison System, reasonably accurate forecasts of load can be made for periods up to four or five years in advance.
3. The two 200,000-kw tie lines between Jamaica and North Queens will be installed in the same trench. The rating is for both lines in service. With only one line in service, its rating can be increased approximately 10 per cent.
4. The reserve capacity available from interconnections of "at least 80,000 kw" was determined by computing the Edison System proportionate share of the over-all savings or reduction in reserve capacity requirements obtained by pooling of reserves through interconnections. The computation involved, therefore, a determination of the reserves required by each company without interconnections and comparing this result with the lesser reserve required by the combined, interconnected systems.

Michelson makes an interesting comparison of situations on the Chicago system with those that parallel the New York conditions.

Of particular interest is that both systems are beginning to use 69,000-volt feeders from generating stations to area distributing stations for very similar reasons.

Emergency Control of System Loads

J. M. COMLY
MEMBER AIEE

C. B. KELLEY
MEMBER AIEE

J. E. McCORMACK
FELLOW AIEE

H. W. PHILLIPS
APPLICATION PENDING

T. W. SCHROEDER
MEMBER AIEE

Introduction*

GENERAL interest in the effect of voltage and frequency reduction on load has increased with the continued reduction in reserve capacity in the last few years. There has been continued discussion of the variation in load with variation in voltage and frequency since the publication of our paper on System Load Swings in 1940,[8] which was presented under the sponsorship of the AIEE committee on power generation.

The difficulty which our industry has experienced in obtaining new generating equipment, together with the rapid increase in load, has contributed to reducing the margin of reserve capacity. Today, a contingency which otherwise would be minor may result in a serious emergency unless means are found to reduce load immediately. Careful study of the effects of variations in voltage and frequency will enable a sound choice of method for load reduction for such emergencies as well as add to our knowledge of the cause and control of load swings.

Surprisingly uniform agreement has been reached based on extensive tests and operating experience on the effect of voltage variations. It is generally agreed that a variation of 1 per cent in voltage results in power load variations of a little over 1 per cent and in reactive load of about $1^1/_2$ per cent. However, substantially no agreement has been reached on the effect of frequency variation.

The purpose of this section of this joint paper is to point out some of the possible effects of attempting to reduce load by dropping frequency and to indicate the need for tests on the particular system before sound conclusions can be reached.

* J. E. McCormack and J. M. Comly.

In broad general principle, a reduction in frequency is synonymous with slower operation of rotating equipment, and at slower speed less work is done. However, there are several factors that might alter this situation and make it unsafe to draw the obvious conclusion that system load may easily be dropped by simply reducing frequency.

In general, a reduction in voltage will reduce lighting and other resistance-type power loads but will have lesser effect on motor loads. It will decrease the system reactive load by reducing the exciting current of the transformers and other induction devices. On the other hand, a reduction in frequency will have little effect on lighting and other resistance loads but might be expected to reduce the power input to motors. As an indication of the wide variation in results to be expected from a reduction in frequency, we offer the results of tests on an individual refrigerator, two different types of common fluorescent fixtures with their lamps and a small distribution-type transformer. Figure 1 shows the variation in amperes, watts, power factor, and watthours per hour with variation in frequency on a popular make of domestic refrigerator when the supply voltage is held constant. The curves clearly show that watts decrease with decreasing frequency, but that amperes and watthours increase. Thus a domestic refrigerator at reduced frequency requires less watts to operate, but since it is required by its control to keep the refrigerator cool, it must do so by running longer and at a lower efficiency. If each of a larger group of refrigerators is in operation longer, the effect will be to increase the number of units in operation at any instant and decrease the diversity. Under such conditions, the coincident load for this and any similar automatic equipment will first decrease, then slowly increase to a load greater than at normal frequency. The reactive load will increase sharply as frequency is reduced and then gradually increase an additional amount as the watts increase.

Figure 2 gives some interesting results of similar tests on two different types of fluorescent lamp fixtures. In both cases, the reactive load increases with decreasing frequency. The single lamp fixture shows an increase of 51 per cent in watts for a 10 per cent decrease in frequency while the double lamp fixture shows no appreciable change in watts.

Tests were made on a 1-kva transformer at frequencies from minus 10 per cent to plus 10 per cent of rated frequency. There was no evidence of saturation. When feeding a constant 100 per cent power-factor load the input did not vary appreciably with frequency, but with a 70 per cent power-factor load the current and watts increased about 1 per cent with 10 per cent reduction in frequency.

Figures 3 and 4 are curves showing a significant part of the data included in our 1940 report of system load swings.[8] This report was concerned principally with the origin of cyclical swings in the load on our interconnected system but two tests were made with Hudson Avenue station separated from the system. Each were short time tests but are interesting in showing the difference in the effect of voltage and frequency variations on the load supplied from that station. Figure 3 shows the variation in station bus power and reactive load with voltage when the frequency is held constant. Figure 4 shows the variation in the power load when the

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J. M. Comly and J. E. McCormack are both with the electrical engineering department of the Consolidated Edison Company of New York, Inc., New York, N. Y.; C. B. Kelley is power pool manager, The United Light and Railways Service Company, Des Moines, Iowa; H. W. Phillips is supervisor, Pennsylvania–New Jersey Interconnection, Philadelphia, Pa.; and T. W. Schroeder is electrical engineer with The United Light and Railways Service Company, Davenport, Iowa.

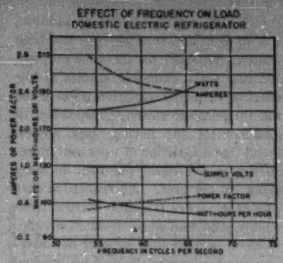


Figure 1. Effect of frequency on load—domestic electric refrigerator

voltage is held constant and the frequency is varied. During the time the voltage reduction test was made the approximate composition of the Hudson Avenue load was 68 per cent lighting and 32 per cent miscellaneous power load. During the time the frequency reduction test was made, the approximate composition of the load was 39 per cent lighting and 61 per cent miscellaneous power.

These estimates of load composition are neither accurate nor detailed enough to be useful except for comparison with similar estimates of load carried by other systems.

A possibility for error in judging the effect of frequency changes on load is the effect of frequency on the accuracy of the switchboard meters. The direct error for indicating instruments due to frequency change is probably small except in the case of induction-type instruments such as watthour meters, but an appreciable error may be caused indirectly by the fact that the burden, or load, on the potential and current transformers supplying the instruments is changed by the change in frequency. Although the changes are small in percentage, the load drop due to frequency alone is also small in percentage.

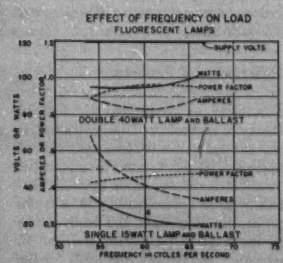


Figure 2. Effect of frequency on load—fluorescent lamps

All of the tests described previously, as well as our general discussion, point to the probability that variations in the composition of the load will materially effect the results obtained by voltage or frequency changes. As none of us have been able to obtain the composition of our system loads with any degree of accuracy, it appears necessary either to accumulate sufficient test data on which to base judgment or to adopt the simpler procedure of obtaining necessary load reduction by using voltage as the control. From past experience, it seems apparent that the control of load by voltage change provides a more definite and convenient method in most cases than such control by frequency change. Voltage is a parameter which can be changed in a local area comparatively easily without affecting an entire interconnected system while frequency is system wide, unchangeable in one local area without an immediate effect on the entire system. Voltage reduction reduces reactive load as well as kilowatt load, while frequency reduction may increase the reactive load at a time when reactive capacity is a limit as well as real power capacity.

Another, and perhaps a more important consideration in the effect of frequency reduction, is that most of our stations operate as a part of an interconnecting system which in turn is connected to other systems. Frequency is the one parameter common to all parts of the interconnecting systems so long as they remain connected. Any attempt to obtain load relief for one shortage area by frequency reduction requires a uniform adjustment of generator throttles throughout the interconnection so as not to affect seriously the loading of interconnecting tie lines. Certainly, it will create new problems on systems using automatic load-frequency control equipment.

Let us suppose that in a particular case, we may reduce the frequency without reaching a limit imposed by the operation of the interconnection. Any station auxiliaries, that are electrically driven, will slow down and may tend to reduce the station capacity at a time when maximum generation is required. The frequency reduction will be accompanied by a voltage reduction caused by the slowing down of the generators, accentuated in certain cases by the slowing down of direct connected or electrically-driven exciters which supply the generator fields. If the necessary adjustments are made to hold the station bus voltage to its scheduled values, there may be a tendency for the voltage at the load to rise due to the decrease in the inductive reactance of feeder reactors, feeders, mains, and services. With reduced reactance in these circuit elements, the voltage difference between the station bus and the service for a given load will be less than at normal frequency. At the same time, the reactive load on the bus may increase since the exciting current of all transformers and similar inductive devices will increase at the reduced frequency. For these reasons, the full effect of a change in frequency on the voltage at the customer's service point cannot be accurately judged by observation of station bus voltmeters.

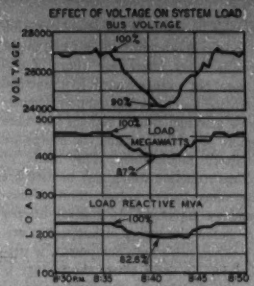


Figure 3. Effect of voltage on system load

A further consideration for a large interconnection is one of revenue. When frequency reduction is made, the load will be reduced throughout the whole interconnected system. In most cases, the load relief required would be on a small part of the interconnected area, and there may be no need for load reduction on the remainder of the interconnection. In contrast to this, load relief by a reduction in voltage can be applied to the local shortage areas without creating a loss of revenue in the other areas.

The conclusions we draw from our study

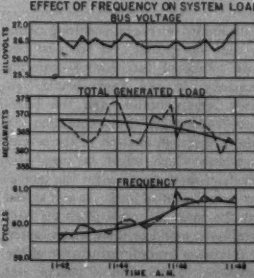


Figure 4. Effect of frequency on system load

are that the effect of frequency variation on load is of academic interest and might provide many interesting hours of discussion, but voltage control as a practical operating tool to obtain temporary emergency load reduction is simpler and more flexible of application and the effects are definitely predictable. The limits of voltage reduction which may be used are determined by the voltage limits of customer's equipment. In general, 10 per cent below the rating of lamps, motors, and other customer's equipment, is permissible. In areas where the voltage is already low, the reduction cannot be as great. In any event, the stability of the system is not affected by the voltage reduction and the reactive component of the load is reduced as the voltage is reduced. Reduction in frequency may cause serious difficulties with reactive load and may reduce system capacity. Under special conditions, where motor loads constitute a large percentage of the total system load, a combination of voltage and frequency reduction may prove desirable.

The use of voltage or frequency reduction for load relief should not be considered as a permanent consideration for system planning, but should be used only as a means for meeting unusual temporary emergency conditions until a normal safe margin of reserve capacity can be restored. The authors of the other papers in this series are presenting results of extensive system load tests which will provide much needed practical data on the effect of voltage and frequency variations on these particular systems. Papers previously published on this general subject may be found in the Bibliography.

Voltage and Frequency Reduction as Tools for Handling Emergency Capacity Conditions*

The limited reserve in generating capacity that has existed over the past two years prompted the electric operating companies in the Pennsylvania–New Jersey Interconnection and the Baltimore–Washington Interconnection to make a thorough analysis of the several procedures which might be used over periods of short duration for the temporary curtailment of load. It was agreed that the most favorable plans for load curtailment would be those which caused the minimum over-all disturbance of the customers. Application of voltage and frequency reduction within limits will affect customers very minutely. However, the over-all reduction in load on a large system becomes sufficient to equal the output of the largest generator.

Several tests have been made in our area to determine the reduction in load which results from the lowering of voltage and these have indicated that a drop of 1 per cent in voltage will give an average reduction in load of 1 per cent. This has been verified by tests and actual experience on other systems.

We had previously found that by lowering frequency 1 per cent, the load would be reduced by 2.6 per cent. However, how much of this reduction was synchronous and asynchronous load and how much was resistance load was not known. Tests were conducted in December 1947 and January 1948 to provide additional information. Of the total load reduction of 2.6 per cent, 1.9 per cent results from reduction of synchronous and asynchronous load, while 0.7 per cent accrues from the dropping of resistance load.

On the basis of these extensive tests, we have been able to set up a definite procedure for utilizing these operating tools for load control during emergency conditions of short duration.

General

The limited reserve situation which developed during and since the war made it necessary to extend co-ordinated operation to the 17 interconnected operating companies in the Pennsylvania–New Jersey and Baltimore–Washington Interconnections. The geographical area in our co-ordinated operating group includes the entire state of New Jersey, the eastern half of Pennsylvania, the northern section of Delaware and the Baltimore-Washington area. The coincident peak load in this area in December 1947 was 5,100,000 kw. In addition to operating on a basis of maximum economy, the co-ordination was extended to conservation of scarce fuels, conservation of fuels in areas that were short and the scheduling of maintenance to assure a minimum outage of equipment with preference given to that equipment which was most badly in need of repair.

In spite of this co-ordinated operation, it became apparent that multiple forced outages might necessitate the curtailment of load. Other areas in the United States confronted with this same problem had investigated the possible load relief which might be obtained by lowering voltage. Voltage drop had also been used during the war in both England and on the continent to curtail load and, in addition, a drop of from 4 to 5 per cent had been made in frequency for the same purpose. We had made extensive studies of the effect of frequency variation prior to the war for purposes entirely independent of load control and during these studies found that a variation of 1 per cent in frequency would cause the load to vary about 2.6 per cent. We realized at that time that part of this drop accrued from voltage reduction on unregulated circuits. However, at that time we were not interested in where or why the load was reduced and made no further effort to analyze the results.

During the reserve deficiency which has existed during the past few years, many discussions have arisen among electric utility operating men as to just what could be accomplished through frequency reduction. Almost everybody agreed that the dropping of frequency would reduce load, but many maintained that the load reduction was caused primarily by the drop in voltage which accompanied the slowing down of generators. This, however, was not the opinion of the men from England and Europe who had been utilizing frequency drop to control load for an appreciable period.

The divergence of opinions on the subject indicated that we would be justified in conducting additional research on the effect of frequency reduction and, consequently, plans were made for making a special frequency drop test. There are 317 generators with a total capacity of 5,560,000 kw in our co-ordinated operating area. No automatic frequency devices are employed at any time since the tie line capacity is exceptionally high and the frequency is at all times under the control of the interconnection dispatcher. The combined area load is exceptionally steady on clear days between 1:30 p.m. and 2:15 p.m. and, therefore, this period was considered most acceptable for conducting a frequency drop test. The normal load during this period of the day was 4,500,000 kw.

Description of Procedure

In order to assure that no material change of load occurred during the test, it was decided to run the frequency high at approximately 60.25 cycles for a period of 15 minutes and then drop it about one-half cycle to approximately 59.75 cycles for another 15 minutes, and finally return to the high frequency for the last 15 minutes. In this way, the loads for the first and last periods could be checked one against the other and any material change

* H. W. Phillips.

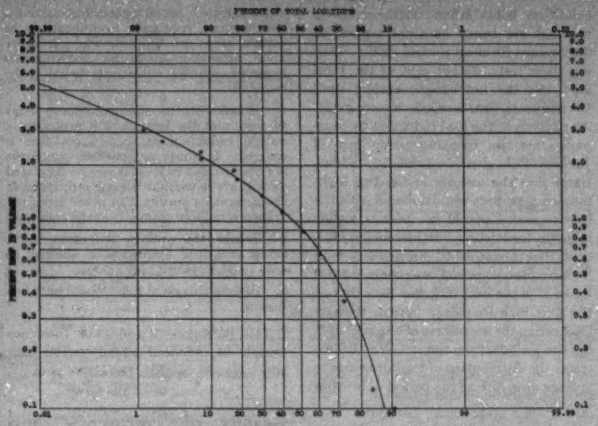


Figure 5. Test number 2 voltage drop probability on unregulated circuits with 0.487 per cent frequency drop

determined. All companies were instructed to take 15 minute integrated readings on all generators and to read voltage every 5 minutes. It was planned to obtain voltage readings at representative locations on the system with an equal division between regulated and unregulated circuits.

The Conowingo Hydro Electric Station of the Philadelphia Electric Company which has a normal capacity of 252,000 kw was utilized to handle the major portion of the frequency control, although other hydro capacity in the area was also utilized in order to obtain the desired frequency change in the minimum amount of time. All other stations were instructed to allow their generation to vary on a basis of governor response.

The first test was made on Friday, December 12, 1947, and the results showed that with a frequency drop of 0.4432 cycles or 0.735 per cent, there was a load drop of 83,500 kw or 1.88 per cent of the average load of 4,437,000 kw for the high-frequency periods. Expanding this frequency drop to a full 1 per cent would indicate a load drop of 2.588 per cent. Unfortunately, there was a misunderstanding regarding the request to take voltage readings on the regulated circuits and at only four of the 106 locations were voltages read on the regulated side of these circuits. While the over-all load drop of 2.588 per cent for a reduction in frequency of 1.0 per cent confirmed prior tests, insufficient data had been taken to determine the load drop resulting from the accompanying voltage reductions.

The second test was conducted on Wednesday, January 14, 1948, when the combined load was 4,500,000 kw or 1.45 per cent higher than at the time of the first test. The period and method of conducting this test was exactly the same. Through better planning, we were able to attain a slightly greater frequency drop of 0.4866 cycles or 0.807 per cent. This resulted in a load drop of 97,000 kw or 2.169 per cent. This figure when expanded to a frequency of 1 per cent, gives a load drop of 2.688 per cent. The calculations of the load drops for both of these tests are of equal accuracy and, therefore, they have been averaged giving a load drop of 2.613 per cent for a 1 per cent drop in frequency.

It is interesting to note that these tests substantiate the results given in a paper prepared by H. Estrada and H. A. Dryar[2] entitled Regulation of System Load and Frequency.

Allocation of the Load Drop

In the second test, voltage readings were taken at 75 unregulated locations and the voltage drop varied from zero to 3.0 per cent. The voltage drops at these various locations have been grouped according to magnitude and plotted on a probability curve, Figure 5. This curve is exceptionally interesting in that it shows that for 10 per cent of the cases the voltage drop was approximately zero. For 50 per cent of the cases the voltage drop was less than 0.92 per cent and for the other 50 per cent of the cases, the voltage drop was greater than 0.92 per cent, thereby indicating that the most probable voltage drop for unregulated locations was 0.92 per cent. This is somewhat less than the average voltage drop which was 1.02 per cent.

The results of the readings taken at 37 locations on the regulated side of regulated circuits showed that the average voltage during the reduced frequency was slightly higher than the average voltage during the high-frequency periods. Seventy-eight per cent of the Public Service Electric and Gas Company's load is regulated. That company reported voltage readings on 15 different regulated circuits and the average voltage on all of

Table I. Load Reduction during Frequency Drop Test

January 14, 1948 (All Data In Megawatts)

	Generation, First Period	Generation, Second Period	Generation, Third Period
Public Service	1090.8	1092.8	1032.7
Philadelphia Electric	1423.0	1329.5	1451.8
Metropolitan Edison*	390.8	415.0	414.8
Sub total	2904.6	2837.3	2899.3
Sub total corrected	2918.6	2828.3	2914.0
Pennsylvania Power and Light	556.6	547.0	556.2
York Haven	9.2	9.6	9.6
Baltimore-Washington	1008.2	1018.2	1029.1
Grand total	4492.6	4403.1	4508.9
Average frequency	60.2884	59.810	60.3048
Corrected load for first high frequency period			4492.6
Corrected load for second high frequency period			4508.9
Sum			9001.5

Average high frequency load $= \frac{9001.5}{2} = 4500.7$

Average high frequency load = 4500.7
Average low frequency load = 4403.1
97.6 = 2.169 per cent
Average freqaency drop = 0.4866 cycles = 0.807 per cent
Load drop = 2.688 per cent for 1 per cent frequency drop.

* Less York Haven.

Table II. Load Drop Due to Voltage Drop During Frequency Drop Test

January 14, 1948 (All Data in Megawatts)

Public Service	
Total regulated load = 827 × 0 =	0.0
Total unregulated load = 233 × 1.37 per cent	3.2
Total	3.2
Philadelphia Electric	
Total regulated load = 770 × 0 =	0.0
Total unregulated load = 770 × 0.567	4.4
Total	4.4
Pennsylvania Power and Light	
Total load (unregulated) 556 × 0.79	4.4
Metropolitan Edison	
Total regulated load = 16 × 0 =	0.0
Total unregulated load = 384 × 1.68	6.5
Total	6.5
Baltimore-Washington	
Total Washington = 350 × 1 per cent =	3.5
Baltimore regulated = 175 × 0 =	0.0
Baltimore unregulated = 508 × 92	4.7
Total	8.2
	3.2
	4.4
	4.4
	6.5
	8.2
Total Drop due to Voltage =	26.7

these circuits during the low-frequency period was 0.09 per cent higher than during the average of the two high-frequency periods. Approximately 50 per cent of the Philadelphia Electric Company's load is regulated. That company examined charts from 22 regulated circuits and the average voltage on these circuits during the low-frequency period was 0.03 per cent higher during the low-frequency period.

A special super-sensitive voltmeter was connected to a 4,000-volt regulated feeder in the Baltimore area and no change in voltage was detected as a result of the change in frequency. The only exception regarding voltage change on regulated circuits was in Washington, D.C., where a reduction of 1 per cent in voltage was reported with the frequency drop of 0.4866 cycles. This data was given its proportional weight in the development of the results of the test.

When making the calculations of load drop with frequency variation, it is important that adequate attention be given to the measurement of time. It has been stated previously that the load drop which accompanied the frequency drop in the tests, herein described, was determined from 15-minute integrated readings. The large majority of these readings were taken on electrical time and about 50 per cent of the total load was measured by electric printometers. When the system frequency is running low, printometers will record more than the true load, while when the frequency is high, the opposite will be true. The same condition holds when watthour meter readings are based on time expired as determined from an electric clock. During these tests, 25 per cent of the total load drop did not become evident until the necessary time corrections were made. An analysis of the load reduction by companies for the frequency drop test of January 14 is given in Table I. It illustrates how the average of the two high-frequency periods was compared with the low-frequency period in order to determine the load drop. Furthermore, it indicates that the total load during the second high-frequency period was only 16,000 kw or 0.36 per cent different than that of the first high-frequency period, thereby illustrating the steadiness of the load during the period of the test. Table II illustrates the distribution of load drop due to voltage drop during the period of low frequency.

Conclusion

It is generally conceded that a 1 per cent reduction in voltage will be accompanied by a 1 per cent reduction in load. While a voltage reduction as much as $7^1/_2$ per cent will be generally satisfactory, there are certain locations where it is impractical and the voltage drop must be limited. This establishes a maximum load reduction of about 240,000 kw or a little more than 6 per cent for our co-ordinated operating areas.

A one per cent drop in frequency independent of a planned voltage drop will reduce the load approximately 100,000 kw or approximately 2.6 per cent. Of this 100,000-kw load drop, 70,000 kw or 1.9 per cent is due to the reduction in frequency alone and 30,000 kws or 0.7 per cent is due to the voltage reduction which accompanies the frequency reduction.

Based on the results of these tests, we have been able to establish a definite procedure of both voltage and frequency reduction for the respective operating areas in order that we may carry on under emergency conditions of short duration with a minimum of inconvenience to our customers. The outline of this program for the Pennsylvania–New Jersey Interconnection is as follows:

1. The normal spinning reserve for this area is 236,000 kw.

2. When the spinning reserve drops below 100,000 kw, the interconnection dispatcher issues a "first voltage warning" to all companies, who in turn communicate it to generating stations and attended substations.

3. When the spinning reserve drops below 50,000 kw, the interconnection dispatcher issues a "second voltage warning." This is communicated to all generating stations. Furthermore, regulators in substations in certain areas are blocked, while in other areas, supervisors go to the substations to supervise voltage reduction, should it be required.

4. If and when the spinning reserve drops to zero, and the frequency is dropping, a voltage reduction up to $7^1/_2$ per cent is ordered. In those areas where regulators are blocked and in areas where no regulated circuits are in use, the voltage is dropped at the generating station. In other areas the voltage is reduced manually at the regulator.

5. Should the voltage reduction prove inadequate, the frequency is allowed to drop to 59.4 cycles (1 per cent).

6. Beyond this point, load would be interrupted.

The Baltimore–Washington Interconnection has adopted a load control plan which closely parallels the above plan.

Results of Frequency and Voltage Reduction Tests on an Isolated System*

As in the case of a majority of the electric utility companies in this country, the Kansas City Power and Light Company, serving the metropolitan area of Kansas City, Mo., and surrounding territory, was faced with the problem of a potential shortage in generating capacity. This is due to the inability of utility companies in general to secure added generating capacity during the recent war years and the unprecedented growth in system load following the close of the war. Further, the inability during the war, to remove generating equipment from service for the usual overhauls resulted finally in the need for a program of scheduled outages for maintenance.

Under these conditions, together with the anticipation of still further load increases but before added generating capacity could be installed, it was decided in the early part of 1947 that emergency conditions might require the lowering of system frequency and voltage.

To determine the amount of relief that could be expected from these expedients, tests on the system were conducted on February 20 and March 7, 1947 to actually measure the amount of load decrease that would obtain for a given drop in system frequency and voltage.

The System Tested

The 1946 peak load on the Kansas City Power and Light system was 235 megawatts. All of this load was generated in

* C. B. Kelley and T. W. Schroeder.

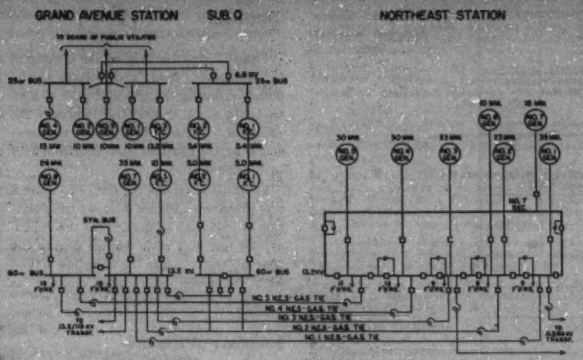


Figure 6. One-line diagram of Kansas City Power and Light Company

System as of time tests were made

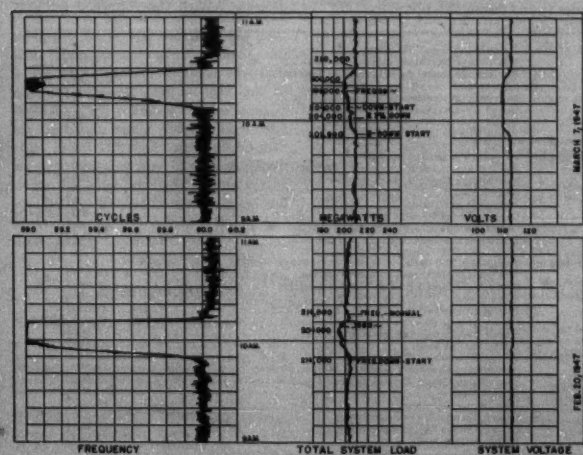
the two steam plants known as the Grand Avenue Station, with a total generating capability of approximately 106 megawatts, and the Northeast plant with a maximum capability of approximately 166 megawatts. Figure 6 is a simplified 1-line diagram showing these generating plants and the principal connections between them. About 13 megawatts of the system load was 25-cycle traction system requirement of the Public Service Company supplied from Grand Avenue Station. Practically all of the 60-cycle load is fed out of the plants at 13.5 kv to nine large distribution substations, and five synchronous converter stations feeding the downtown d-c area. An idea of the character of the system load may be obtained from the following breakdown:

Type of Load	Per Cent of System Peak
Industrial	44.7
Domestic	14.6
Commercial	27.1
D-C Edison system	7.7
Other utilities	5.9

A stand-by interconnection with the system of the city of Kansas City, Kan., is maintained but there is no normal interchange of power over this tie.

Figure 7. Frequency, load, and voltage charts obtained during tests

Top—March 7, 1947 test
Bottom—February 20, 1947 test



Method of Making Tests

Previous observation indicated the system load was practically constant between 9:30 a.m. and 11:00 a.m. Fortunately this condition prevailed on the days during which tests were made. The tie with the Kansas City, Kan., system was open during the tests.

System load readings were taken from a Leeds and Northrup strip chart totalizing recorder which totalizes the output of each generator on the system. Frequency readings were taken from a Leeds and Northrup frequency recorder. The frequency recorder readings were checked by use of a portable frequency meter and also by measuring the speed of one turbo-generator set by use of a geared tachometer. Prior to the tests the charts of the frequency recorder and the load recorder were adjusted to be in time synchronism.

The system frequency was dropped gradually in approximately uniform steps over the period of the tests. Prior to voltage dropping test men were stationed in those substations having feeder voltage regulators to make the regulators nonautomatic at a given time. Immediately following this the generating plant voltage was dropped in approximately uniform steps by simultaneous changes in setting of the generator voltage regulators. Voltage was held at test values under guidance of a portable indicating voltmeter.

February 20, 1947 Test

The frequency was lowered slowly requiring about 15 minutes time to reach a value of 58.5 cycles.

A brief summary of the load conditions existing prior to and during the test follows:

9:49 a.m.—Frequency normal, 60-cycles
System load, 214 megawatts
Northeast generation, 138 megawatts
Grand Avenue 60-cycle generation, 62 megawatts
Grand Avenue 25-cycle generation, 14 megawatts
Public Service load, 13.7 megawatts

9:49½ a.m.—Operators at each station started lowering frequency

10:05 a.m.—Frequency, 58.5 cycles
System load, 204 megawatts
Northeast generation, 132 megawatts
Grand Avenue 60-cycle generation, 59.5 megawatts
Grand Avenue 25-cycle generation, 12.5 megawatts
Public Service load 12.5 megawatts

10:10 a.m.—Operators at each station started increasing frequency

10:14 a.m.—Frequency normal, 60 cycles
System load, 214 megawatts
Northeast generation, 135 megawatts
Grand Avenue 60-cycle generation, 62.5 megawatts
Grand Avenue 25-cycle generation, 16.5 megawatts
Public Service load, 13 megawatts

Total load dropped as result of decreasing frequency 10 megawatts
Drop in frequency was 2½ per cent
Drop in load was 4.68 per cent

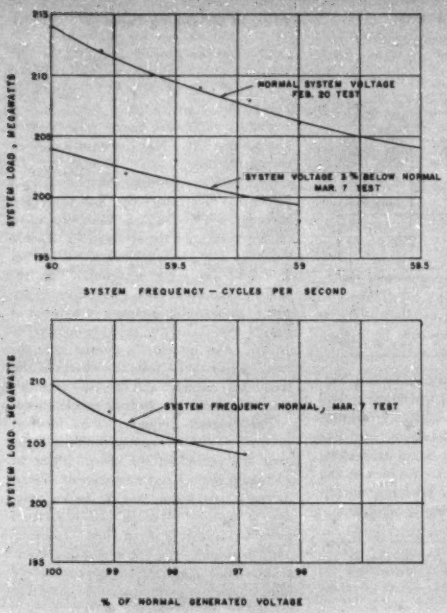


Figure 8. Curves prepared from test data

Top — Variation of load with frequency

Bottom—Variation of load with voltage

During the tests, additional readings and observations were made of boiler and turbine auxiliaries. No instability in operation of this equipment was observed at either station. The reduction in frequency was not noticed by the customers as not a single complaint was received.

March 7, 1947 Test

As in the previous test the condition of very nearly constant system load prevailed prior to the time selected for the test.

The test was conducted in two steps. The first consisted of lowering the 60-cycle bus voltage at Northeast Station, and the 60-cycle and 25-cycle bus voltages at Grand Avenue Station, approximately 3 per cent. The second step consisted of lowering the system frequency one cycle, while maintaining the bus voltage at 3 per cent below normal.

A few minutes prior to the start of this test, the voltage regulators on all 4,000-volt feeders out of substations having feeder regulators were set on "nonautomatic," in the position in which they were found.

As a safety measure neither Northeast Station or Grand Avenue Station were fully loaded during this test. The Kansas City, Kan., tie was opened a few minutes prior to the test, and remained open during the entire test period.

The lowering of voltage and frequency resulted in no complaints from the customers, and mechanical department reported that conditions in the boiler rooms were stable.

Following are shown the system conditions relative to frequency, voltage, and load in each of the generating stations prior to and during this test:

9:51 a.m.—Frequency normal, 60-cycles		
Bus voltage Northeast number 1 section	13,830	volts
Bus voltage Grand Avenue number 1 section	13,600	volts
System load	209,900	kilowatts
Northeast generation	134,000	kilowatts
Grand Avenue 60-cycle generation	60,000	kilowatts
Grand Avenue 25-cycle generation	15,900	kilowatts
Public Service load	14,500	kilowatts
10:00 a.m.—Frequency normal, 60-cycles		
Bus voltage Northeast number 1 section	13,390	volts
Bus voltage Grand Avenue number 1 section	13,200	volts
System load	204,000	kilowatts
Northeast generation	130,000	kilowatts
Grand Avenue 60-cycle generation	60,000	kilowatts
Grand Avenue 25-cycle generation	14,000	kilowatts
Public Service load	13,300	kilowatts
10:16 a.m.—Frequency 59.0 cycles		
Bus voltage Northeast number 1 section	13,415	volts
Bus voltage Grand Avenue number 1 section	13,200	volts
System load	198,000	kilowatts
Northeast generation	126,500	kilowatts
Grand Avenue 60-cycle generation	58,000	kilowatts
Grand Avenue 25-cycle generation	13,500	kilowatts
Public Service load	12,600	kilowatts
10:32 a.m.—Frequency normal, 60 cycles		
Bus voltage Northeast number 1 section, normal	13,800	volts
Bus voltage Grand Avenue number 1 section	13,600	volts
System load	210,000	kilowatts
Northeast generation	135,000	kilowatts
Grand Avenue 60-cycle generation	60,500	kilowatts
Grand Avenue 25-cycle generation	14,500	kilowatts
Public Service load	12,500	kilowatts

Test Data

Figure 7 (bottom) shows the portion of the frequency, load, and voltage charts obtained during the period from 9:49 a.m. to 10:14 a. m. during the February 20, 1947 test. Figure 7 (top) shows the same charts for the March 7, 1947 test from 9:51 to 10:32 a.m.

From these charts the data of Figure 8 was prepared. Figure 8 shows the variation of load with frequency for the February 20 test and with voltage during the March 7 test and also the variation of load with frequency at reduced voltage as obtained from the March 7 test.

Summary of Results

During the February 20 test, with system load fairly steady at 214 megawatts and normal frequency of 60 cycles, the frequency was gradually dropped over a 15-minute period to 58.5 cycles and the load dropped to 204 megawatts. This represents a frequency drop of 2.5 per cent and corresponding load drop of 4.68 per cent, or about 1.87 per cent load reduction per cent frequency reduction, or approximately 0.31 per cent per 0.1 cycle frequency reduction.

On March 7, the same system was tested to determine the effect of dropping voltage and also the effect of dropping frequency with lower than normal voltage obtaining. With a load of 225 megawatts and normal voltage the voltage was dropped 3 per cent below normal and the load decreased to 218 megawatts, 1 per cent load decrease for 1 per cent voltage decrease. With the voltage 3 per cent

below normal and the load at 218 megawatts the frequency was dropped 1 cycle and a further load decrease of 6.5 megawatts resulted, or about 1.78 per cent load drop per cent frequency drop under the reduced voltage condition.

Conclusions

The results of the tests of February 20 and March 7 indicate that system load reductions in appreciable amounts can be safely effected by lowering system generated voltage and frequency.

Based upon a system load of 225 megawatts, the following results can be expected with certain changes in voltage and frequency as shown below:

1.
Assuming a system load of 225 megawatts.
Lower system primary voltage 3 per cent with 4.0 kv automatic station regulators on nonautomatic.
Result—load decrease of 7 megawatts.

2.
Assuming a system load of 218 megawatts and voltage 3 per cent below normal.
Lower system frequency one cycle.
Result—load decrease of 6.5 megawatts.

This is a total drop in load of 13.5 megawatts, or approximately 6 per cent.

It should be possible to lower the system primary voltage as much as 5 per cent, and at the same time decrease the system frequency $1^1/_2$ cycles. Using the same base value of 225 megawatts and assuming the same conditions as stated before, a reduction of approximately 21 megawatts, or 9 per cent, could be expected, provided the regulators in the substations are set on nonautomatic. If sufficient time is available to do so, the regulators in the automatic stations can always be set on the nonautomatic position. However, an emergency might arise where this could not be done, in which case the per cent drop in load due to decrease in the voltage would not equal 1 per cent, but would possibly be in the order of 0.3 to 0.6 per cent drop in load for each 1 per cent drop in voltage. Even under this condition, the lowering of the voltage and frequency is a practical and effective method of relieving the system under emergency conditions.

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Discussion

E. E. George (Ebasco Services Incorporated, New York, N. Y.): In the tests reported in the paper under discussion, it does not appear that use has been made of a method developed by the Northwest Power Pool for the elimination of major errors due to gradual changes in the total of customer's loads during the test. This method consists in varying frequency and voltage back and forth repeatedly so that a plot of the results would give a sawtooth or zigzag effect. By drawing curves through the upper and lower series of points, there is established a band whose average width is a good measure of the true effect of voltage or frequency reduction on generating requirements.

There now seems to be general agreement on the effect of voltage reduction. Perhaps the system engineering committee may now consider this project finished with satisfactory results. There is no general agreement on the effect of frequency reduction and there likely can be none except in cases where the percentage and type of power load is very similar. It would be interesting to secure reports of tests from some of the companies in the southwest, where power load is predominant.

One reason some systems give for lack of interest in frequency reduction is that in most cases frequency reduction is either difficult or impractical. However, frequency reduction is going to occur even when not intended and engineers should know what is going to happen. Unfortunately, they have made most of their tests to find out what would happen on customer's premises and the resultant effect on generating requirements. Sufficient information has not been obtained on the effect of low frequency on generating plant auxiliaries. The loss of auxiliaries generally means a heavy interruption of service to customers. In some cases, this is far more important than what low frequency does to customer's equipment. It is suggested that the committee promote tests on power plant auxiliaries. It is desirable to known how present designs of plants perform, what minor changes can be made at slight expense, and what major changes in design would be required to take care of emergency frequency conditions as low as 57 cycles.

Eric T. B. Gross (Illinois Institute of Technology, Chicago, Ill.): It is very interesting that these valuable papers are in fairly good agreement with each other. It is difficult to predict the changes of composite loads without tests, since the change of the motor input depends largely on the motor type and on the torque-speed characteristics of the load. We know how each type of load reacts under voltage—and/or frequency drops, but the behavior of mixed loads of unknown composition can be found only by tests.

It seems to me that voltage reduction is somewhat simpler than frequency reduction. It is realized that the problem of today's emergencies is more of a megawatt problem than a megavolt-ampere problem. In other words, we lack boiler capacity in steam plant and water supply in some hydro-systems. However, it should be kept in mind that frequency reduction without equal per unit voltage reduction leads to increased reactive megavolt-ampere, and a word of caution seems appropriate with regard to the rating of the equipment in megavolt-ampere. Certainly, the measures discussed here will lead to a decrease in megawatt, but we may not always be able to permit an increase of current or megavolt-ampere that could be brought about by frequency drops.

James M. Comly and H. W. Phillips: It is most gratifying to the authors that their paper has aroused the general interest indicated by the excellent discussions which have been presented. We are particularly happy to receive the information from T. E. Curtis of the Pacific Gas and Electric Company concerning that company's experience with extended periods of reduced frequency operation.

In response to the first comment by E. E. George of Ebasco Services, it should be pointed out that a paper by Estrada and Dryar[1] described the method which he mentions for eliminating major errors due to gradual change in the total of customers loads during tests. Phillips, in our paper, notes that the results of the tests made by the Pennsylvania–New Jersey and Baltimore–Washington Interconnection substantiate the results given in the earlier paper by Estrada and Dryar. We agree with George that further test and design data are necessary to get reliable data on the effect of reduced frequency on station auxiliaries and through them on station capacity. The experience of Commonwealth Edison mentioned by T. E. Gault adds to the available data.

The question raised by Professor J. R. Hafstrom of the University of Wisconsin concerning equipment dependent on instantaneous frequency for their operation is very disturbing since it indicates a tendency on the part of designers of specialized equipment which may lead to disastrous results. We want to make it very clear that an electric power utility is designed to produce and to sell kilowatts and kilowatt-hours for light and power, and is not designed to supply or sell precision frequency or time. Large additional investment and considerable redesign would be required to make our present systems suitable for supplying precision frequency and time in addition to power and light.

It is true that in their normal operation the power utilities usually hold frequency so closely to its nominal value that timing devices which depend upon system frequency for their accuracy may be used satisfactorily. It is also true that under normal conditions electric systems operate in synchronism over wide areas. However, both of these conditions exist because they aid the utilities normally in the safe and economic operation of the systems to supply power and light. In an emergency, any part of an interconnected system may be separated from the rest of the system electrically without warning to the customers and may be operated as an isolated system for an indefinite period. Similarly, changes in frequency several times greater than normal may occur at any time without warning to individual customers. The designers of radar flight control devices or any other devices which must operate continuously to protect life or property should know that there can be no guarantee that utility system frequency will remain constant. If it does remain constant it is for the benefit of the operators of the system and not as a part of the service the utility undertakes to supply its customers.

Eric T. B. Gross of the Illinois Institute of Technology in his discussion emphasizes the fact, mentioned in the introduction to our paper, that reduction in frequency without corresponding reduction in voltage will cause an increase in reactive load. Because of this fact the megavolt-ampere rating of the generators must be considered as well as the megawatt rating.

The discussions as well as the data presented in the paper confirm the belief that voltage reduction is usually a safer and more convenient method than frequency reduction for shedding load in an emergency brought on by the loss of reserve generating capacity. In special cases, a combination of voltage and frequency reduction may be desirable. Such a special case might be a system of such extent and design that it does not require automatic frequency with tie line load bias control. Another example might be a system having a load of such composition that the load is unusually sensitive to changes in frequency. In most cases, however, frequency reduction may lead to reduction in capacity, unnecessary loss of revenue, and undesirable complications in the operation of system ties.

References

1. See reference 2 of the Bibliography in the paper.

T. W. Schroeder and C. B. Kelley: The discussions seem to conclude that while voltage reduction is a satisfactory means of load reduction, frequency reduction is not similarly so. It is evident that the effectiveness of frequency reduction is quite variable and dependent upon the make-up of the composite system loads. It would appear that the load decrease with frequency increase, result of the tests reported by Comly, is due to a predominance of refrigeration load on the portion of the Consolidated Edison system tested. On the other hand our tests, though of short duration, and Phillips' results indicate load reduction with frequency reduction. The results reported by T. E. Curtis of Pacific Gas and Electric Company indicate load reduction with frequency reduction during the critical drought period affecting his company's system last spring. This, in spite of a considerable portion of pumping load, regarding which it is reasoned that if frequency is reduced, the pumps run slower, but run longer until the right amount of water is obtained.

Contrary to the feeling that voltage reduction is preferable because it would affect fewer customers, it is our belief that if practicable, frequency reduction would be best from the customers' standpoint because it would affect all customers only slightly. Further, if frequency reduction on an interconnected system is possible, a comparatively small frequency reduction might result in a reduction of load of a large number of kilowatts, although of only a small percentage of a large load.

E. E. George's comments regarding the desirability of determining the effect of fre-

quency reduction on power plant auxiliaries, particularly at times when the plants are running near absolute maximum capability, is worth-while. At present we consider the possibility of a reduction in plant output to be the chief reason for dropping frequency no lower than to about 58.5 cycles during emergencies.

Further knowledge regarding performance of auxiliaries would point the limiting equipment and the actual lower limit of frequency reduction.

Continuous Load-Frequency Control for Interconnected Power Systems

H. M. DIMOND
ASSOCIATE AIEE

G. S. LUNGE
ASSOCIATE AIEE

Synopsis: Functioning in response to integrated deviation from a tuning fork-controlled standard frequency, and biased by deviation from scheduled tie-line load as telemetered to this location, this improved load-frequency control equipment provides continuous comparison of system frequency and tie-line load with their respective scheduled values. This continuous comparison, in conjunction with strict proportionality between deviation and correcting effect on the prime mover governor speed level adjustment, has proved in several years of service to be beneficial in minimizing governor and inlet valve movement and hence wear, as compared with the periodic intermittent correcting impulses used in earlier types of frequency controllers, even of the integrated-deviation type. The use of integrated frequency deviation as the quantity to which the controller responds inherently eliminates frequency deadband, but the tie-line load controller retains an adjustable deadband to render it oblivous of inevitable minor oscillations in tie line loading.

LOAD-FREQUENCY control is the automatic regulation of the frequency of a power system, or portion thereof, to keep the frequency within a narrow range and at the same time maintain approximately constant the kilowatt loading of one or more tie lines leaving the system, or portion thereof.[5,8] Where no such tie lines exist or their loading need not conform to a definite schedule frequency control only is applicable.[1,2] Where no attempt is made to have the generating station or stations concerned share automatically in the task of maintaining the system frequency approximately constant but maintenance of tie-line loading is considered important, tie-line load control only may be used,[2,4,6,10] either in response to the voltage vector phase angle existing between corresponding voltages at opposite ends of the interconnection[4] or, more commonly, to the totalized kilowatt loading of all tie lines between the two systems or portions thereof.

Today in extensive system interconnections, instead of assigning the task of frequency control exclusively to one station, it is the usual practice[11,12,14] to provide automatic load-frequency control equipment for all stations that are to have automatic regulation of output, in order that all stations controlled may participate to some extent in the task of maintaining system frequency constant. This procedure usually takes the form of tie-line bias control,[8,13] in which the automatic controller responds neither to frequency only nor to tie line load deviation from schedule only, but to a combination of the two, in adjustable proportion. With this type of control, all of the controllers on the system will not be satisfied until the frequency is correct and all tie-line load schedules are being fully maintained. However, if both the frequency and tie-line loading depart from scheduled values, the controller will not initiate correction of frequency at the expense of causing further deviation from scheduled tie-line loading, or vice versa. Instead, if the deviation from tie-line load scheduled value is in such direction and amount as to be certain to disappear automatically if and when the frequency returns to normal, the controller will not initiate any correction but will let nature take its course, with consequent substantial reduction in wear and tear of prime mover governor and valve mechanisms as compared with endless conflicting action of tie-line load controllers and frequency controllers. The former device cannot perceive whether the tie-line load change from schedule is such as to disappear automatically later without controller action, and the latter device tends to make the same correction for a given load change on the system, regardless of whether the load change occurred on the system regulated or on the neighboring system at the other end of the tie lines, two situations requiring entirely different treatment if tie-line load schedules are to be maintained as closely as possible.

Purpose of Load-Frequency Control

Close maintenance of 60-cycle frequency by power systems has been found to be of major assistance in many phases of system operation, particularly in maintaining scheduled power interchange required by contractual relationship with sources of purchased power or with customers having some generating capacity of their own. The operation of electric clocks, time switches, and other synchronous motor driven devices is obviously facilitated by closely regulated frequency being maintained, though this is usually a minor aspect of the application of such equipment.

If automatic frequency control equipment were applied to a large enough proportion of the generating capacity connected to the system, occasional manually-performed changes in prime mover output would become unnecessary. However, so far, most power systems using automatic load-frequency control equipment depend on it only to take care of minor variations in system load that would have an adverse effect on the frequency and would be extremely exacting in their demands on any human operator who might attempt to correct for such changes. Accordingly, the usual present practice is to make the load-frequency control equipment relatively slow in response so that

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H. M. Dimond is development engineer and G. S. Lunge is with the central station engineering division, both with the General Electric Company, Schenectady, N. Y.

the controller will only intervene in the so-called fourth stage of system load division upon the occurrence of a load change. The first stage of load division takes place in a manner determined by the electrical impedances from the generators to the load that has changed, the second stage in a manner determined by the amount and geographical distribution of stored energy in the generator units with their associated prime movers, and in the third stage the action of the speed governors modifies the load division in accordance with the governor characteristic slopes. In the fourth stage of load division, the governor characteristics are modified by their synchronizing motors under control of the load-frequency controllers.

If all tie lines were of ample carrying capacity and stations under automatic frequency control well distributed geographically throughout the area, aside from exceptional situations where contractual relationships fix tie-line interchange power schedules, there would be no need for tie-line load control. With the advent of system interconnection on a larger scale, and the insistence on more efficient operation of tie lines, the tendency in recent years has been to insist on load-frequency control rather than frequency control, sometimes called flat frequency control.

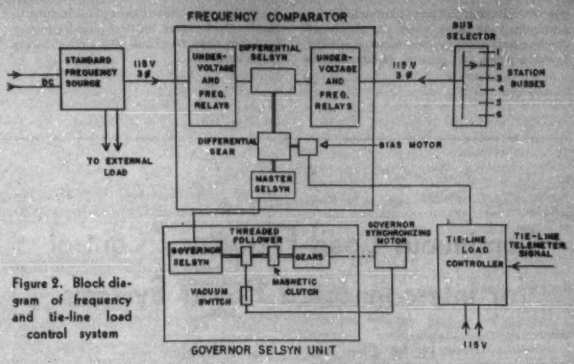


Figure 2. Block diagram of frequency and tie-line load control system

Basic Types of Frequency Control Equipment

In this country, frequency control equipment has been usually of one of two types. The most generally used type responds to deviations from instantaneous frequency and automatically corrects the prime mover speed level settings at a rate at least approximately proportional to the deviation from normal frequency (60 cycles). The standard reference frequency usually takes the form of an a-c frequency measuring bridge as used in conventional frequency recorders. When, for more accurate clock operation, automatic time error correction is required, this is accomplished by automatically recalibrating the frequency bridge in the frequency controller, with the aid of a time error indicator operated from a tuning fork controlled source of standard frequency.

In another type of automatic frequency control equipment, the prime mover governors are automatically readjusted in proportion to the integrated deviation from the standard reference frequency. In the usual construction of this type of frequency control equipment, the load on each unit controlled is made directly proportional to a few seconds time error that is intentionally permitted to accumulate between the condition of daily minimum load and daily peak load, this time error disappearing automatically as the system load decreases. This makes it unnecessary to measure the output of each controlled unit and convey this information back to the frequency controller.

Intermittent versus Continuous Frequency Control

From the nature of its operating principle, that is, frequent periodic comparison of system frequency with the standard value, frequency control systems func-

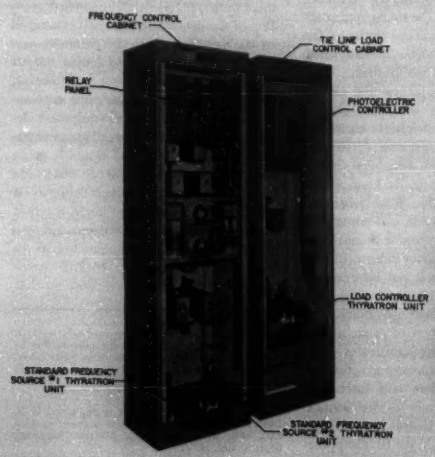
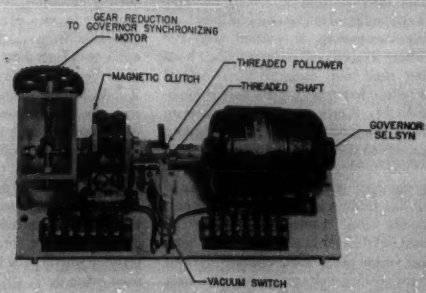


Figure 1. (left) Rear of control cabinets. Cabinet containing two separate frequency controls on left. Right cabinet for one tie-line load control

Figure 3. (below) Governor Selsyn unit with cover removed



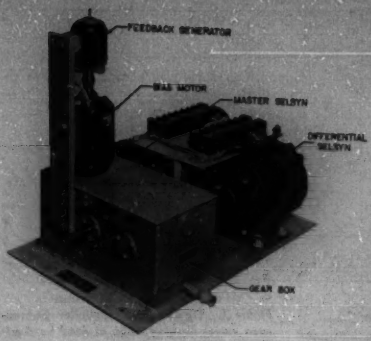


Figure 4 (left). Frequency comparator unit

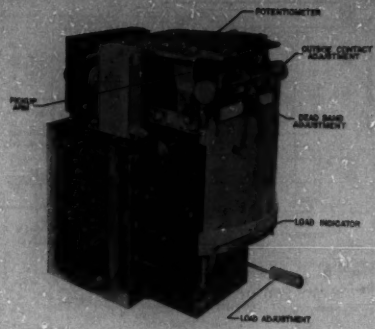


Figure 5. Photoelectric tie line load controller removed from case

tioning in response to deviation from instantaneous frequency send correcting signals in the form of impulses to correct the governor settings as required.

In frequency controllers responding to integrated deviation from standard frequency, in which the standard frequency was established by a pendulum, corrections were likewise made in the form of impulses originated, when necessary, on each swing of the pendulum in a given direction.

In more recent years, in order to obtain the advantage of smoother and less rapid prime mover governor and gate operation under automatic frequency control, the integrated-deviation type of frequency controller was redesigned so as to use a tuning fork controlled source of standard reference frequency, instead of a pendulum. This change is analogous to the modern trend toward continuous action generator voltage regulators instead of the intermittent-action type. By means of a differential Selsyn having a 3-phase winding on both stator and rotor, the standard reference frequency and a system frequency are continuously compared, any difference in frequency resulting in rotation of the shaft of this Selsyn. Such rotation of the shaft is communicated by other Selsyns of conventional type to the prime mover governors, ordinarily by means of a governor Selsyn unit that utilizes the normal synchronizing motor as the source of torque for such readjustment but establishes a fixed relationship between the number of revolutions of the shaft of the comparing Selsyn and the number of revolutions of the synchronizing motor resulting therefrom, 60 revolutions of the differential Selsyn (comparator) usually corresponding to a change in governor adjustment from no load to full load at rated frequency (60 cycles).

Such continuous comparison eliminates any deadband in the controller, as even a very minute drift in frequency will be perceived by the differential Selsyn. As in the case of the earlier intermittent type of integrated deviation frequency controller, time error correction is inherent and automatic, so that the only manual time error correction necessary is that occasioned by errors in the standard frequency as compared with correct astronomic time, or corrections necessary as the result of the automatic frequency control equipment getting out of range, that is, having insufficient generating capacity under its control to make the required correction in machine output.

Load-Frequency Control

In its original form, even the continuous type of integrated deviation frequency controller did not take into consideration the tie line loading, that is, was only suited to flat frequency control. The remainder of this paper is devoted to a description of the requirements for which a load-frequency controller of this type was designed to incorporate tie line load bias, and how these requirements were met.

The following general requirements were selected as offering those features most desirable in an automatic frequency and tie-line load system.

Frequency Control

1. Frequency control must be accomplished smoothly with no sudden changes in the governor setting. This feature is very desirable to minimize wear and maintenance on governor and control parts.

2. The frequency sensing device must be sensitive to any frequency deviations no matter how small because a small frequency error maintained for a time can accumulate into a considerable time error.

3. The rate of frequency correction must be proportional to amount of deviation of system frequencies from standard time base.

The Line Load Control

1. Control must operate from telemetered signals.

2. Control must be adjustable for a wide range of operating conditions. It must have adjustments to allow for a range of control of proportional bias rate, maximum bias rate setting, deadband, and base load. The limits, slopes, and so forth for the "raise" side of the dead band and those for the "lower" side must be able to be set independently and any single adjustment must not affect other adjustments by an appreciable amount. The frequency and tie-line load control must be able to operate either as flat frequency or flat tie-line load controls or as frequency control biased by tie-line load. The amount of bias should be adjustable over wide ranges to accommodate a wide variety of operating conditions.

In setting up these specifications the latest concepts of efficient power interchange methods were carefully considered. Another consideration was the important objective of designing a control system which would do the right thing at the right time without the necessity for the system to frequently readjust itself. Figure 2 shows a block diagram for the frequency and tie-line load control system developed to meet the foregoing specifications. The system consists of a standard frequency source, a frequency comparator, a means for transmitting the signal from the frequency comparator to the governor control, a tie-line controller, and a means for converting this tie-line load controller signal to a form which can be used to bias the frequency control signal.

The standard frequency supply consists of a tuning fork oscillator and its associated electronic circuits which control the speed of a motor generator set. In the frequency comparator a 3-phase differential Selsyn provides a frequency sensing device with no deadband. It also provides an output signal

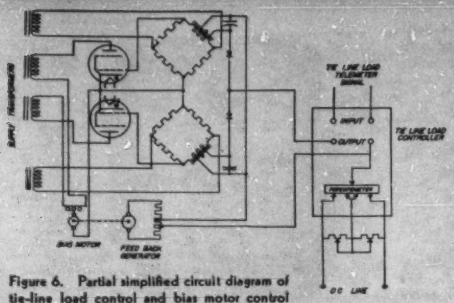


Figure 6. Partial simplified circuit diagram of tie-line load control and bias motor control circuits

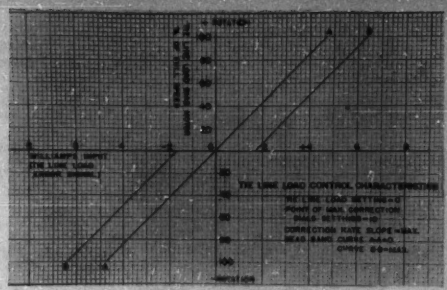


Figure 7. Tie-line load controller performance curves showing range of dead band adjustment obtained by spacing of center contacts

that is proportional to the frequency error signal integrated with respect to time. The 3-phase output of the motor generator set is applied to the stator winding of the Selsyn and the rotor windings are connected to the system to be controlled. As long as the frequency of the two systems is exactly the same, the shaft of the differential Selsyn will remain stationary. Any difference in frequency between the system and the standard will cause the shaft to turn at a speed and in a direction proportional to the amount and direction of the frequency difference. A frequency difference of one cycle per second will cause the rotor to turn at a speed of one revolution per second. The rotation of the Selsyn shaft is transmitted through the differential gear box to the master Selsyn. The signal from the master Selsyn is communicated electrically to the governor Selsyn unit. As the governor Selsyn rotates in response to the signal received from the master Selsyn it turns a threaded shaft. A follower on this shaft is carried along the axis of the threaded shaft, (Figure 3). A single-pole double-throw vacuum switch is operated by the linear motion of the follower and energizes the governor synchronizing motor to adjust the governor in the proper direction to correct for the frequency condition then in existence. As the governor synchronizing motor runs, its action is fed back through a gear reduction to the previously mentioned follower on the threaded shaft of the governor Selsyn unit. The follower is rotated in a direction to return it to its original position, thus opening the vacuum switch and stopping the synchronizing motor. The above arrangement will "remember" the number of revolutions made by the governor Selsyn and cause the governor synchronizing motor to make exactly the corresponding number of revolutions. The "memory" feature nicely eliminates any necessity for correction to allow for the time lag in starting and stopping the synchronizing motor. The governor Selsyn unit is equipped with a magnetic clutch so that under extreme conditions when the governor synchronizing motor has reached the limit of its control range, and the automatic control is still calling for governor adjustment, the magnetic clutch will slip and no damage to the control parts will result. The clutch is also used for disconnecting the control when manual changes of governor setting are made.

During a condition where a machine has reached one of its load limits, time will be lost or gained and a resulting time error will accumulate. In order to conveniently correct this accumulated time error the time bias motor is provided on the frequency comparator unit, Figure 4. The time bias motor operates through the differential gear in the frequency comparator unit and its function is to introduce a small bias so that even though the system frequency is exactly the same as the standard frequency and the differential Selsyn shaft is stationary, the master Selsyn will be rotated very slowly in a direction to steadily but slowly urge the system to correct for the accumulated time error. The motor used for the time bias correction is also used for tie-line load bias and in the latter function the bias motor speed is controlled by the tie line load controller.

The signal to the tie-line load controller is obtained from the new frequency-type telemeter recently announced by the General Electric Company,[17] and the input to the photoelectric load controller is d-c milliamps. The photoelectric load

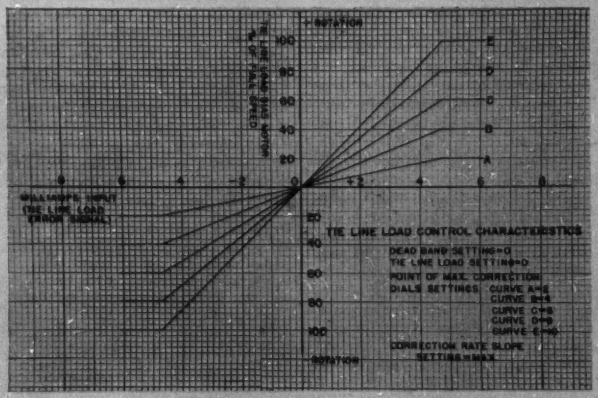


Figure 8. Tie-line load controller performance curves showing range of adjustment of point of maximum correction rate obtained by changing potential across potentiometer

controller is similar in operation to the standard General Electric photoelectric recorder.[14] The recording pen for the photoelectric recorder has been replaced by a contact arm riding on the wire winding of the potentiometer, Figure 5. The mechanical zero set of the basic instrument has been made readily adjustable and is geared to the dial pointer to indicate and set scheduled load on the tie line. In operation the basic instrument zero setting is biased by this adjustment by an amount which will require a telemeter signal corresponding to the scheduled tie-line load to hold the pickup arm at the center of the potentiometer. A deviation from schedule will cause a deflection of the arm proportional to the deviation. The photoelectric recorder system is used merely as a torque amplifier to enable the movement of a low-torque type instrument to be transferred with enough power to move a contact across the potentiometer. The photoelectric-recorder type torque amplifier system provides a very dependable and time-proved method for accomplishing this.

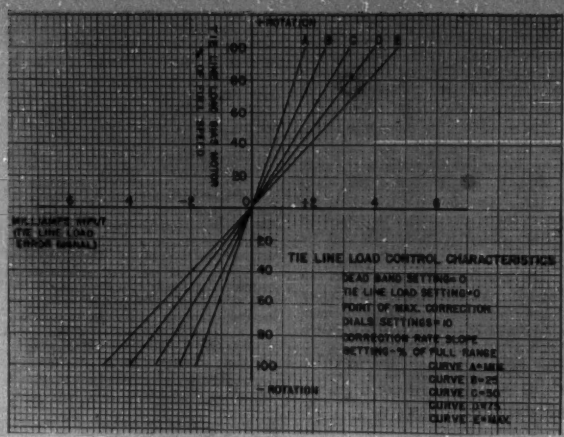


Figure 9. Tie-line load controller performance curves showing range of correction rate slope by adjusting position of outside contacts on potentiometer

Figure 1 shows a view of the rear of the control cabinet and the location of the various components. It should be noted that this particular installation provided two separate frequency controls and one tie-line load control unit. The two frequency control units are duplicates; the tie-line load control is used with either.

Only a partial circuit diagram is shown in Figure 6. The complete circuit diagram is complicated by switching circuits, and so forth, and is not needed for an understanding of the operation of this system. D-c potential is applied to a linear wound potentiometer as shown, so that the exact center of the winding is at zero potential with respect to the circuit and a deflection of the pickup arm to the left would produce a negative potential on the arm and to the right a positive potential. When scheduled tie-line load is being held, the pickup arm will be in the center of the potentiometer and the potential of the arm will be at zero. When the pickup arm is at zero the bias motor is stationary. The potential of the pickup arm for any given deflection is determined by the relative position of the center and outside contact points and the voltages across these two points. Contacts are shown in Figure 5, with the adjusting screws for positioning the contacts. Since the two center contacts are connected together, any distance spanned by them on the potentiometer will be at zero potential and determines the deadband region. For any fixed voltage applied between the center contacts and one of the outside contacts of the potentiometer, the output signal voltage of the pickup arm for any definite deflection will be proportional to the distance between the center contact and the outside contact, thus a wider separation between these two contacts results in a smaller signal increase for any definite increment of tie line load signal.

The position of the outside contact will also determine the deflection point at which the maximum potential will be reached. Thus, from the tie-line load control a signal is obtained which varies in voltage magnitude and polarity as the tie-line load varies and the proportionate amount of this variation is adjustable over a wide range. This signal is used to operate thyratron circuits which control the speed and direction of the load bias motor rotation. The direction of rotation depends upon which of the two thyratrons is operating and the speed will be dependent upon the amount of current passed by the thyratron. Refer again to the circuit diagram, Figure 6. The plate current of these thyratron tubes depends upon the phase relationship between the grid and plate a-c voltages. If the two are in phase a large current will pass and if out of phase no current will pass. The phase relationship between the grid and plate of these tubes is varied by phase shift in the bridge circuit feeding the tube grids. Each phase shift circuit consists of several resistors and a saturable reactor. A small change in the direct current of the saturable reactor control winding will cause a considerable change in the phase of the voltage applied to the thyratron tube grid. Rectifiers in the output of the controller are arranged so that a positive signal from the control actuates one saturable reactor and a negative signal actuates the other. The amount of signal determines the resulting phase shift which in turn controls the output current of the thyratron tube. In order that the speed of the bias motor will be proportional to the signal obtained from the photoelectric control, and will not vary appreciably with motor load, a small feed-back generator is connected to the bias motor. A portion of this generator output is connected to oppose the signal applied to the saturable reactor control winding. This provides, in addition to a speed proportional to the potential impressed on the control circuit, a positive antihunt circuit which operates very effectively. In Figures 7, 8, and 9 are shown the characteristics of the tie-line load control. In these figures full speed of the bias motor corresponds to a correction rate of "no load" to "full load" governor setting in five minutes. These three figures show the range of correction rate settings available and the many adjustments that can be made in this control equipment. They show that the deadband can be adjusted from zero to approximately 20 per cent of full load range, that the correction rate slope can be set for a wide range and the maximum correction rates are adjustable from 100 per cent speed to 0 per cent speed for the

bias motor. With these adjustments, it is possible to meet nearly any operating condition requirement and provide a smooth control of a type which allows a maximum efficiency of operation with minimum requirements for maintenance.

The signal from the differential Selsyn and the signal from the bias motor are combined in the differential gear before going to the master Selsyn so that signal to the governor Selsyn is the sum of the two. The systems may be operated "flat frequency" or "flat tie line load" by switching off one or the other system.

The signal from the master Selsyn can be used to control any number of governor Selsyn units, so the addition of more generators under control requires only the addition of a governor Selsyn unit for each generator.

Conclusions

The continuous-comparison load-frequency control equipment has the following novel features compared with earlier equipment operating on the same integrated frequency-deviation principle:

1. Standard reference frequency is a tuning fork controlled source, instead of being established by a pendulum.

2. Comparison between system and standard frequencies is continuous and results in a proportionate correction in prime mover governor adjustment, instead of taking place only during a brief part of each forward swing of a pendulum.

3. Tie-line bias, in adjustable amount, now is included.

4. The tie-line load controller can operate either on flat tie-line control or tie-line bias control, by throwing a switch.

5. The tie-line load controller, of photoelectric construction, continuously compares the tie-line loading, measured locally or telemetered, with its scheduled value, but only initiates proportional correcting action in case of deviations outside the adjustable deadband.

6. Instead of raise or lower impulses, the load-frequency control equipment originates a continuous proportional correction for the governor Selsyn units, thus making less rapid changes in governor adjustment necessary to accomplish the same resultant change in speed level setting.

7. By telemetering the tie-line load deviation from schedule to several generating stations each containing load-frequency control equipment, several stations can be regulated simultaneously. Due to the good agreement of tuning forks, discrepancies between stations for this reason will cause load changes of much smaller order than inevitable changes due to other causes.

Like earlier equipment operating on the same integrated frequency-deviation principle, the described load-frequency control equipment is characterized by:

8. No deadband for frequency deviations from schedule, even slow drifts in frequency being detected and eventually corrected.

9. No need for bringing a measurement of each controlled generating unit output to the load-frequency control equipment for verifying load division.

10. Time error is corrected automatically daily as long as the controlled generating units remain within their regulating range. Slight differences between astronomically correct time and tuning fork standard time must be corrected manually.

11. Load division between simultaneously controlled units is automatic, requiring no supplementary equipment, as all load increments are divided between machines in strict proportion to their ratings, regardless of any initial intentionally unequal base loads on them, as long as the permissible regulating range of each unit is not exceeded.

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17. A Faster Telemeter for Carrier Current Channels, E. E. Lynch, H. C. Thomas, G. S. Lunge. Electrical Engineering, March 1948, pages 249–52, and AIEE Transactions, volume 67, part 1, 1948, pages 288–94.

Discussion

O. R. Bulkley (Southern California Edison Company, Los Angeles, Cal.): The paper by Dimond and Lunge presents an interesting description of new tie-line load control equipment which would operate on a somewhat different principle than the control which is in quite general use today.

That part of the control for the regulation of flat frequency as described in the paper is essentially the same as the General Electric frequency control presently in use by the Southern California Edison Company for its three 82,500-kw units at Hoover Power Plant. This equipment has been in service since 1939 and its operation has been very satisfactory. Of particular note is the small amount of service and maintenance that has been necessary on the control equipment itself.

It is not felt that there has been any appreciable maintenance required on governors or their servo motors as a result of frequency controller operation, either with the above control equipment at Hoover or with the conventional control in other generating plants on the company's system.

Henry E. Warren (Lombard Governor Corporation, Ashland, Mass.): The paper by the authors describes a device very similar in certain respects to the Precise Turbine Governor recently published.[1]

A hydraulic governor controlling a small turbine with convenient arrangement for load variations and interconnections with the New England Power System through the Boston Edison Company has demonstrated conclusively many of the arguments in both papers.

In the Dimond-Lunge basic controller for flat frequency alone as in the precise governor, continuous comparison is made between system frequency and frequency derived from a standard. In the former case the standard is a tuning fork and in the latter a vibrating wire similar to that used for the control of the Mount Palomar telescope. Both of these frequency standards are capable of very high precision but the tuning fork frequency has a practically fixed value while the vibrating wire is intentionally arranged so that its frequency value can be altered instantaneously over a range of several percent.

The tie between the vibrating tuning fork and the servo-motor of the hydraulic governor in the Dimond-Lunge arrangement is through a motor generator set serving as a power amplifier of the tuning fork frequency and then through a differential Selsyn unit which compares the standard frequency with the generator frequency and through a

transmitting Selsyn to the regular speed controller of the governor which is arranged as a follow-up of the latter Selsyn. In the precise governor the frequency of the vibrating wire is electrically amplified and comparison is then made by a differential Selsyn or an equivalent. The result is transmitted through a pair of Selsyns directly to the governor system without the interposition of a speed control motor follow-up.

The Dimond-Lunge controller introduces load control as a factor to modify speed control only by introducing it in their tie-line control auxiliary. In the precise turbine governor an element of load control is introduced directly with the minimum amount of complication and without requiring any tie-line controller by automatically biasing the vibrating wire. In this way the precise governor will operate with the same general characteristics as existing governors because it develops a speed droop characteristic of the same nature.

It is well to bear in mind that a very large proportion of all governors at present are of the so-called speed droop type operating without tie-line controllers and for the time being new apparatus must live comfortably with this situation.

Tie-line control is desirable without a doubt in a certain proportion of power plants and where such tie-line control is installed either the Dimond-Lunge controller or the precise governor can function satisfactorily. In power stations where automatic tie-line control is a factor the general characteristics of both governors would be about the same although the Dimond-Lunge arrangement seems to be somewhat more complicated.

Reference

1. Precise Turbine Governor, Henry E. Warren. AIEE Transactions, volume 67, part I, 1948, pages 571–4.

C. K. Duff (Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada): The development described in this paper creates a new system of load-frequency control generally applicable to power systems and interconnected networks. The new system based on integrated deviation from a standard reference frequency differs from the system now generally used which responds to deviations from instantaneous frequency. A third system proposed by T. E. Curtis and W. M. Pickslay[1] tolerates a much narrower band of deviation and demands much higher speed of control than either of the others. Three such basically different systems of control are mutually incompatible in any interconnection of power systems, so that any system of control which is to replace one which is already established on extensive power networks must exhibit marked superiority not only in theory but also in ability to meet practical operating conditions. If the new system has been tried in continuous commercial service the results of such tests would be a valuable aid in assessing its merits.

Frequency control based on integrated deviation from a standard pendulum clock, and loading of regulating generators in proportion to such deviation has been tried, formerly on a basis of approximately one second deviation for full load change on the generators. The present paper speaks of "a few seconds" time deviation between maximum and minimum loading. It would be interesting to know if the larger time droop has been found preferable in actual operation or whether the variations of independent time standards dictate a greater time droop in order to avoid unintended load shifting between generators. Time-keeping ability inherent in the integrating principle, with zero dead band, and minimum corrective action to reduce wear of control equipment are advantages experienced by the Hydro-Electric Power Commission of Ontario as described elsewhere by this discusser.[2]

It might have been possible to apply the integrating principle to tie-line load control as well as to the frequency element instead of introducing dead band to minimize control activity. Integration would tend to ensure scheduled energy transfer over ties. However, if dead band is used, its ready adjustability is a decided advantage.

Provision has been made for the inevitable condition of generators reaching their control limits and allowing time to drift beyond the normal band. The biasing motor used for time correction is also used for tie-line control, but the two controls presumably cannot act concurrently, and tie-line load would be left to drift while time bias is in operation. Further, unless the transfer to time bias were made simultaneously at all control stations the effect on tie-line loads might be appreciable. Automatic simultaneous transfer at a predetermined time deviation would seem impractical on account of differences in the reference standards. Concerted manual transfer would be the alternative, not easy on a large interconnection.

It may be noted that the control equipment as described would need to be installed in or near each and every generating station to be controlled, with tie-line telemetering to each of such stations, and the settings and adjustments would need to be made by the operators at such stations. While the instantaneous type of control now widely used may be similarly installed in each generating station, it may be and frequently is installed at a central supervisor's office where operating adjustments and maintenance may be more readily co-ordinated.

The "continuous" nature of the control stems from the smooth continuous motion of the differential Selsyn and bias motor, but the benefit of this, as compared with intermittent control is probably small, at least in hydroelectric generator control, because of governor damping. In any case "continuous" control probably breaks down at the governor, where the synchronizing motor is operated by contacts controlled by the governor Selsyn, and since there must be some clearance or dead band at these contacts, the synchronizing motor must operate in short pulses much as it would by intermittent control. The one great advantage of the governor Selsyn control is to ensure synchronizing motor travel proportional to the control signal, thus overcoming one of the difficulties in applying any kind of measured control to conventional governors.

Other characteristics of governors and turbines tend to handicap any type of automatic control unless specially dealt with. In particular there are:

1. Deadband in the governor due to friction, lap, slack linkages, and so forth.
2. Sluggish response due to dashpot damping.
3. Nonlinear power-gate characteristic.

However, these characteristics, which are difficult to eliminate, will be less detrimental to a relatively slow-acting control as described in this paper than to quicker-acting types.

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1. Preliminary Consideration of System-Wide Load Division by Means of Accurate Time Control, T. E. Curtis, W. M. Pickslay. AIEE Transactions, volume 67, part II, 1948, pages 1563–70.

2. Control of Load, Frequency, and Time of Interconnected Systems, C. K. Duff. Electrical Engineering (AIEE Transactions), volume 64, November 1945, pages 778–86.

Thomas W. Brown (Lombard Governor Corporation, Ashland, Mass.): The device described by Dimond and Lunge, in employing continuous comparison against a reliable standard, is unquestionably advantageous. In its employment of several relaying steps between the comparing unit and the turbine valves or gates, however, there may be hindrances to optimum performance. Recent work in a number of places, for example that of T. E. Curtis,[1] indicates that it is desirable for frequency comparison controllers to approach the rapidity of action of the "anticipation" type of control. In the authors' reference 17, it is stated that the telemetering system responds in less than one second; when to that fractional second time is added the lag of the governor speed control motor and that of the device, here described, for translating the receiver signals into a movement of the governor Selsyn, the over-all response time could become well over a second. This would establish a minimum dead band for rapidly changing system conditions, with the possibility of incorrect response if the changes are sufficiently rapid.

It would be of interest to know what is the range of the vacuum switch. If this is at all large, there must result a sort of step-response which would not help smoothness and continuity of action.

It would be of interest also to have a description of the device which accomplishes the time-correction function described by the authors. This would appear to be a separate unit feeding the bias motor jointly with the tie-line controller.

This paper significantly supports the argument that much can still be accomplished by a co-operative endeavor on the several parts of power system operating engineers, instrument designers, and governor builders, to produce an integrated and close-coupled regulating system specifically designed to fit the most desirable operating practices.

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1. Power System Governing, T. E. Curtis. Electrical Engineering, volume 66, December 1947, pages 1197–1200.

George S. Lunge: In his discussion of this paper, Duff states that because this system of frequency control perceives integrated deviation, rather than instantaneous deviation, from the standard frequency it must be incompatible with previously used automatic frequency control systems that respond to instantaneous deviations in frequency.

As in the case of earlier equipment (reference 1 of the paper) operating on the same basic integrated frequency-deviation principle, the authors see no reason why any difficulty should be experienced in interconnected operation if this type of frequency control is used on only one of the systems concerned, the remaining systems having automatic frequency control responding to instantaneous deviation in frequency, provided that comparable rates of correction are used on all of the systems involved. In fact, due to the smooth action of the control described, such interconnected operation should be accompanied by less severe tie-line power swings than those occurring when two systems, each having independent frequency control responding to instantaneous deviation in frequency, are interconnected.

The accumulated time deviation of one second from no load to full load has been quite generally used. It has been found that any drift in tie-line load due to difference between independent time standards is insignificant compared with tie-line load changes due to other causes, such as manual adjustment, discrepancies in governor performance, and so forth.

It is true that time bias cannot be introduced simultaneously with tie-line bias. It is customary to correct any accumulated time error daily at a time not likely to be inconvenient, such as shortly after midnight.

Although decentralized load-frequency control is inherently more expensive and less convenient to adjust than centralized control, it has the definite advantage of permitting independent control of operating areas in case tie lines trip out, and thereby reducing the time required to reestablish interconnected operation.

In reply to the point, mentioned by both Duff and Brown, regarding the intermittent action of the governor Selsyn unit, it should be noted that the system described is well suited also to direct Selsyn adjustment of the speed level setting of the governor, if the latter has a speed adjusting Selsyn with suitable gear ratio. The governor Selsyn unit is applicable to either steam turbines or hydraulic turbines; only the latter ordinarily have been provided with a speed level adjusting Selsyn as an optional feature.

Warren has called attention to the many points of similarity between the system described in his paper[1] and that described in the present paper. As pointed out by Bulkley, the frequency control portion of the equipment described in the paper has been in successful operation since 1939.

Brown's discussion emphasizes the need for rapid response in the telemetering system used with any automatic load control system and the advantage of eliminating further time lag in control by replacing the governor Selsyn unit (acting on the synchronizing motor) by direct Selsyn adjustment of the governor speed level, mentioned before as readily possible with this system.

The range of the vacuum switch in the governor Selsyn unit is mechanically adjustable. This is adjusted to a setting depending on the speed of the synchronizing motor and on the ratio of the feed-back gears.

As mentioned by Duff, the same motor is used both for tie-line bias purposes and for (occasional) time bias purposes. Since the two types of bias may at times call for opposite action, there would be nothing gained by using two separate motors for these functions.

Reference

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Evolution of Ward-Leonard Control for Shovels and Draglines

P. S. STEVENS
ASSOCIATE AIEE

IT IS quite fitting that the first electric shovel which the writer has been able to trace was shipped to the Milwaukee Railway and Light Company on July 5, 1902. This machine was built by the Vulcan Steam Shovel Company of Toledo, Ohio. It was equipped with a 1/2 cubic yard dipper, traction wheel mounting, and a boom-type front-end. Alexander Backus, then President of the Vulcan Company, was a personal friend of Thomas A. Edison and there seems to be no doubt that Mr. Edison was influential in persuading Mr. Backus to consider electric drive at that time. This shovel probably operated from a d-c trolley circuit.

A shovel presents a variety of problems to tax the ingenuity of the motor and control application engineer. The three main motions of a shovel, hoist, swing, and crowd, have entirely different characteristics. The hoist motion, which today is universally rope operated, pulls the dipper through the bank for loading, raises the load to the dumping position, and lowers the empty dipper for repeating the cycle. In addition to the usual problems in a mine hoist or skip hoist, a shovel hoist adds the design requirement of being able to stall under load without an interruption in power. The dipper teeth may strike an unyielding object, such as poorly blasted rock or an unforeseen boulder, but the motor must continue to pull even though stalled with the controller wide open.

The swing motion consists of rotating the machine about its center, moving the dipper back and forth between the digging area and the dumping position. The swing motion may be likened to a short-cycle transportation application, accelerating and decelerating a heavy mass. The crowd motion is auxiliary to the hoist in that it determines the depth of cut for digging, and the reach for dumping. The crowd also must be able to stall with the controller wide open without damage to the mechanical shovel or the electric equipment. Shovels today are mounted on crawlers, generally driven from the hoist motor.

This paper will cover what is usually known as the "three motor shovel" where each of the three main motions, hoist, swing, and crowd, are driven by an independent variable-speed motor. By contrast, Diesel power usually drives the three motions through clutches. Such machines may be driven also by a single-induction motor.

In the early days of building electric shovels, from 1902 to 1920, both a-c and d-c motors were used with rheostatic control. In the case of direct current, series wound motors were applied, and with alternating current, variable-speed wound-rotor induction motors were common. The first record of an a-c machine of this type is a four yard dipper dredge, very similar to a shovel, built in 1906. This machine had wound-rotor induction motors and was arranged to operate from a power supply of 2,080-volts 3-phase 25-cycle. It was shipped to H.R.H. Maharajah, Jammu, Kashmir, India, with two 18-inch hydraulic dredges. It is interesting to note that even at this early date, engineers appreciated the difficulty of ventilating a motor which was frequently stalled, and to solve the problem they used forced ventilation. Also the advantages of lower flywheel effect resulting from using twin half-sized hoist motors, a common practice today on larger machines, was appreciated at that time.

Prior to 1920 there existed in the electric shovel industry a battle between alternating and direct currents, very much the same as existed on the question of railroad electrification. Since most power supply was alternating current, the d-c motor shovel required a motor-generator set. The a-c shovel usually was driven by 440-volt motors with transformers on board, to step down from 4,000 or 2,300 volts.

Today the 3-motor shovel is offered in sizes two cubic yards and larger. Four cubic yards and smaller shovels are available also as friction-type machines driven by Diesel or gasoline engines or single electric motors. Draglines with variable-speed motors on the several motions are available in $2^1/_2$ cubic yards and larger sizes, and in sizes ten cubic yards and smaller are available with Diesel-drive. The smaller sizes are sold with gasoline engines and single electric motors.

The first Ward-Leonard electric shovel was put into operation in 1920. It might be added that the term "Ward-Leonard" in the shovel industry refers to a d-c motor driven by its own individual generator, whereby the torque and speed of the motor are determined by controlling the field of the generator. This type control is sometimes referred to as "variable voltage control." One of the principal advantages which this type control offered was a more effective and permanently adjusted means of causing the motor to stall down and continue to pull at a predetermined maximum torque without interrupting the circuit. The steam engine had accomplished this result in a most excellent manner due to its limited boiler pressure. With the earlier rheostatic control using both alternating and direct current, the stalling torque was limited, after a fashion, by using "notch back" or "jamming" relays. Such devices were not fully reliable, however, on account of

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P. S. STEVENS is with the Bucyrus-Erie Company, South Milwaukee, Wis.

wear and tear, and lack of understanding on the part of operating personnel in the early days of electric shovels.

Differential Series Field

With the Ward-Leonard control, the stalling torque limitation was obtained by using a differential series field on the generator. It gave to the electric motor the steam engine characteristic in a foolproof fashion, and with means which would remain reasonably permanent in adjustment. Differential compound generators were used and their drooping voltage is well known in comparison with the flat compound and shunt generator characteristics. The drooping voltage with increasing current causes the shovel motor to stall down and still continue to pull hard without abusing itself or the mechanical parts of the shovel.

The earlier Ward-Leonard electric shovels were equipped with series motors, a carry-over from the rheostatic control. The series motor is an excellent tool for digging and hoisting, but it is not so effective for braking operation or lowering the dipper. However, after considerable experimenting, the series motor was made to lower the dipper reasonably well, and in spite of its shortcomings, some of the original Ward-Leonard series motor machines are still in operation. For instance, I had the pleasure this year of seeing in operation in a coal mine in Ohio, a series motor equipped Ward-Leonard machine which I witnessed begin its life on the Iron Range in northern Minnesota in 1920. The old machine is still doing a creditable job.

Three Field Generator

The next important development had to do with the generator. The original shovel generator was made with two fields, one separately excited shunt-type field which was varied for motor speed control, and a differential series field with a fixed number of turns. The usual commutating fields of course were used. The objection to this characteristic was that when the motor began to pull hard, the voltage of the generator was too low and the digging speed entirely too slow. The corresponding steam shovels would dig much faster. The problem was put up to the generator designer who solved it by adding a third field, a self-excited shunt field which raised the voltage characteristic effectively especially at operating loads. This change gave the shovel the well known "3-field generator."

The first Ward-Leonard shovel had a no-load generator voltage of about 330 applied to a 230-volt motor. The self-excited field increased the no-load voltage to about 400. Over the years, generator and motor designs have improved so that no-load voltages of 575 volts are common in shovel equipment. This has resulted in faster operating speeds without increased sizes of motors. In the shovel industry, we use for Ward-Leonard equipment specification purposes, the continuous horsepower rating of the motor at 230 volts. Actually in service the generators develop approximately 460 volts at the motor continuous current. Hence, if anyone attempts to compare the horsepower of a Ward-Leonard shovel with a Diesel-driven shovel for example, he should increase the electric motor continuous rated horsepower by 100 per cent before comparing with the horsepower of a Diesel engine on a similar size shovel. The same increase must be made before comparing with some applications of a-c motors to shovels and dredges.

Referring again to the series wound motors and analyzing the characteristics brings out several objections. The heavy armature circuits had to be opened for reversing, and this required large contactors. The control when plugging or lowering the dipper was rough on commutators. To accomplish reasonable operation required a variable generator differential field, armature resistors, armature contactors and generator self-excited field contactors to insure reduced generator voltage before opening the armature circuit. The operation timing of these devices was determined by rotation direction switches on the machinery shafts. The result was a considerable layout of what would be termed today "gadgets." In shovel operation, with its relatively short cycles, lowering, braking and deceleration rank closer in importance to acceleration and pulling than in many other applications hence attention was turned to the shunt motor which can hold back as readily as it can pull.

Shunt Motor

A shunt-type motor with a separately excited field has not as much peak torque per ampere as a series motor. However, the proper size of motor to drive a shovel motion is not determined by the motor's peak capacity. It may seem strange to many of you that the proper size of motor is determined by its continuous load carrying thermal capacity. Shovel motors in most cases are forced ventilated to improve the continuous rating, and with such have more than ample peak or stall capacity with separately excited fields for shovel operation. Hence, the separately excited or shunt-type motor offered excellent possibilities for simplifying shovel control as well as greatly improving operating characteristics.

With the separately excited motor, reverse rotation is obtained by reversing the generator separately excited field. Negative torques are handled just as readily as positive torques and without gadgets. High-speed empty dipper lowering is accomplished by raising the generator voltage to a maximum, and weakening the motor field somewhat. Plugging for stopping is smooth and trouble-free. Control became simplicity in the extreme. Motor and generator armatures were connected solidly with no resistors, contactors, or knife switches.

The first completely shunt motor equipped shovel was put into operation in 1925 as a seven cubic yard coal loading machine for a stripping mine in Montana. The advantages of the shunt motor were realized immediately by operating people as well as designers, and while for several years there were still arguments about whether series motors or shunt motors were better, in 1928 the shunt motor became universal. The considerable regeneration of power when lowering the dipper or stopping the swing motion was sufficient to require a 15 to 20 per cent downward revision of kilowatt hours per cubic yard excavated. It might be interesting to know that the large electric stripping shovel today equipped with balanced hoist, which will be discussed later, will dig and move a cubic yard for 0.3 of a kilowatt hour. Draglines are not so efficient as will be pointed out later.

An early typical installation of Ward-Leonard control with shunt motors and three field generators is shown in Figure 1 with schematic diagram and a tabulation of individual field adjustments for each point on the controller. This form was very effective in training field erecting and operating personnel. In the early days a shovel motion would be reported as slow and an inspection by a factory engineer would disclose that the self-excited generator field was not connected. Such experiences were eliminated by calling for definite voltage readings by the erector on the self field connected alone as called for on this form.

Motor Lowering of the Dipper

It was the practice with early railroad-type steam shovels to lower the dipper by declutching the engine and catching the falling dipper on a mechanical brake.

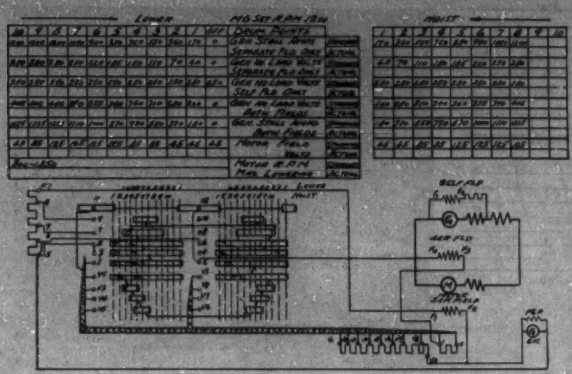


Figure 1. Early shunt motor control with schematic diagram and field adjustments

On larger steam stripping shovels appearing later, it was the practice to back the dipper down part way on the engine using a release throttle, and then after nearing the digging position throw out the clutch and lower the remaining distance on the brake.

The first a-c stripping shovels, six to eight cubic yards, put into operation in 1917, accomplished the complete lowering cycle of the empty dipper by means of the motors. The hoist machinery, however, was equipped with clutches and brakes as for steam operation. The early rheostatic d-c stripping shovels lowered somewhat after the fashion of the steam shovel, part way on the motor and part way on the brake. At that time all smaller electric shovels lowered completely on the brake.

Figure 2 is a kilowatt input chart of one of the earlier a-c stripping shovels using motor lowering. The chart reads from right to left and shows to the right of point "A" a regenerating portion of the cycle below the zero line; then follows, before digging, the high peak required to stop the motors from reverse synchronous speed and accelerate them to forward hoisting speed. This particular chart was made of a shovel loading cars at high lift, and shows quite a long hoisting cycle after the rugged digging portion. The peak load to the left of dumping is hoisting the empty dipper out of the car so that the open door clears the side of the car, and the kick-off power to accelerate the motors in the downward direction before regeneration begins.

Up to 1925 no attempt had been made to lower the dipper of a close-coupled fast-cycle shovel on the motor. However, Ward-Leonard control with the shunt motor offered new possibilities. Heretofore, all electric shovels were in reality electrified steam shovels. The first real electric shovel was designed in 1925, and the hoist machinery was equipped with only a small disconnecting clutch so the motor could be used for propelling. It was not equipped with a large size brake suitable for lowering the dipper. The design contemplated motor lowering. This was the first four cubic yard full revolving shovel which replaced the old heavy-duty fast-cycle railroad-type shovel common in this country.

The first demonstration of the motor lowering feature of this shovel was rather disappointing to many, because it was so smooth it appeared slow. It was not as spectacular as dropping a dipper and catching it on a brake. The opponents lost account of the fact that often the brake lowered dipper would be carried in a high position during a considerable portion of the return swinging cycle, and then dropped fast to make up for lost time. With motor lowering it was only necessary to begin the lowering stroke immediately after dumping the load into the car, and even at a reduced top speed, compared to gravity falling, the dipper would be down as fast as the swing was over to the digging position. Motor lowering is much easier on the operator because he accomplishes the complete hoisting and lowering operation with the controller handle, and he is relieved of foot brake and clutch operation. It is also easier on the machinery, hoist rope, and entire shovel. Motor lowering has become, even on the smallest Ward-Leonard shovels, practically universal today.

Figure 3 shows the kilowatt input to a Ward Leonard hoist on an eight cubic yard shovel with motor lowering. The cycle shows clearly the digging, hoisting, dumping, and lowering operation. The high peak regeneration at the end of the lowering strike is due to stopping the motors at the bottom of the pit. It will be noted that the amount of regeneration with the Ward-Leonard is considerably more than in the case of the a-c shovel because regeneration with the Ward-

Figure 2. Kilowatt input to hoist motion of pioneer motor lowering shovel driven by a-c induction motor

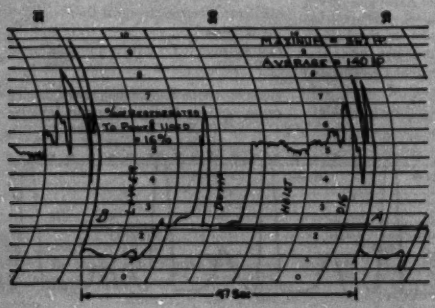
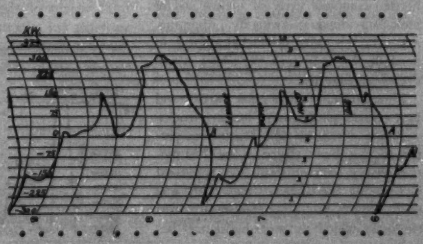


Figure 3. Kilowatt input to hoist motion with motor lowering and Ward-Leonard control



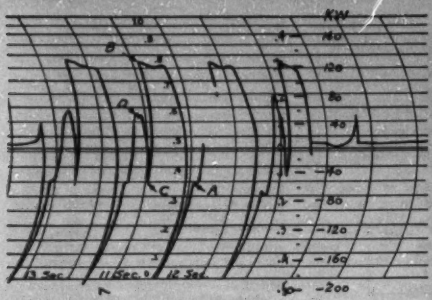


Figure 4. Power input to four yard shovel hoist raising and lowering empty dipper from top to bottom

Figure 5. Five cubic yard quarry and mining-type shovel loading cement rock

Leonard takes place right down to standstill, whereas with alternating current, regeneration takes place only when running above synchronous lowering speed.

New Motor with Reduced Flywheel Effect

Shunt motor Ward-Leonard control was such an improvement in permitting fast and smooth plugging that it focused attention on the fact that the shovel cycle could be improved if motors with reduced flywheel effect were available. In the early days, electric shovels were properly classed as slow in comparison with steam shovels, because it required too much time to accelerate and decelerate the motor armature. The steam engine accelerated to speed almost instantly.

The first hoist motor on the newly developed four cubic yard electric shovel of 1925 had a WR^2 of 1,040 pound-feet squared. It could make four cycles per minute hoisting and lowering the empty dipper which is a good measure of the effectiveness of motor lowering. The first model of a new line of motors developed shortly thereafter had a WR^2 of 437, and permitted five cycles per minute of the hoist motion. A kilowatt chart of this operation is shown in Figure 4. The normal complete cycle of such a shovel including digging, swinging, dumping, and return is three cycles per minute in good digging. In rock the speed is two cycles per minute.

Figure 5 shows a five cubic yard shovel in a cement plant quarry loading trucks.

Figure 6 shows the revolutions-per-minute of a shovel motor on the hoist motion clearly indicating the high rpm used in lowering the dipper. This chart was taken by a recording mechanical tachometer which did not distinguish between positive and negative rotation.

A third line of motors known as the new *600 Series*, is now available for shovel operation. A comparison of the three lines is shown in Table I covering the size used on the original four cubic yard hoist in 1925, and a five cubic yard hoist today. The 1925 shovel grew to five cubic yards several years ago.

Each motor is smaller physically than its predecessor, a valuable improvement for a compact shovel layout.

The Balanced Hoist

The next major improvement made in electric shovels and closely related with shunt motor Ward-Leonard control was the balanced hoist used for the first time in 1930 on a 16 cubic yard stripping shovel. The separately excited motor having the characteristic of continuing to run at about the same speed even if the torque load is reversed from negative to positive permits the effective use of a movable counterweight to help hoist the dipper. The arrangement of such mechanism is shown schematically in Figure 7. Such shovels have heavy ballast at the rear for stability, and to use some of this ballast as live counterweight does not add to the total weight of the machine. The advantages of counterweights are well known in elevators and hoists. A counterweight permits the use of a smaller size motor to do the same work and greatly improves the safety condition in case of power or brake failure. On a shovel, however, there are other advantages not found in the elevator or hoist.

For example in the "lowering" section of Figure 7, the balancing point between empty dipper and live counterweight occurs when the bail pull reduces to 45,000 pounds. It is easily understood how the bail pull reduces as the dipper lowers, because when the dipper handle reaches a vertical position, the pull on the hoist rope is zero as the entire dipper and handle weight are then supported by the shipper shaft. This means that below the balancing point, the hoist motors must lift the counterweight to permit the dipper to continue in its lowering direction. Tracing through the lowering operation, the hoist motor controller handle is thrown into reverse direction after dumping, and there is an input kick-off, during which period the motors are motoring to accelerate themselves until the weight of the falling dipper overhauls them and the motors begin to regenerate and hold back.

Figure 6. Motor speed during normal digging cycles, showing high-speed motor lowering with Ward-Leonard control

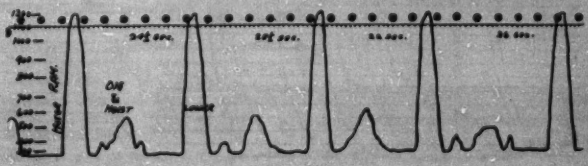


Table I

	Frame	WR^2 Pounds Feet	Horsepower Continuous Rating, Unblown.	Horsepower Continuous Rating, Blown.
Original motor	109	1,040	140	
Improved motor, 1926	418	437	150	187½
New motor, 1948	616	365	150	187½

The effect of the falling dipper weight is high at the beginning of the stroke, and combined with the electric power input accelerate the motors quickly. At the bottom portion of the lowering stroke, the bail pull is reduced to such an extent that the effect of the counterweight is then used in addition to electric power to stop the motors. The controller handle is thrown into the hoisting direction before the motors reach the bottom of the stroke. The result is that the counterweight and electric power stop the motors quickly at the bottom and accelerate them again into the hoisting direction. The effect of this is just the same as if a subway train were permitted to run downhill when leaving a station during acceleration in order to reach a high speed quickly, and then run uphill during deceleration to stop quickly at the next station.

The reduction in both motor size and power demand with the counterweight is self-evident. Also the kilowatts drawn from the line are less and the kilowatts pumped back into the line are less, because the counterweight has the effect of reducing both. The spread between positive and negative power peaks is reduced.

There is still another advantage of the counterweight in connection with the electric power use. This comes about from the fact that some power companies that

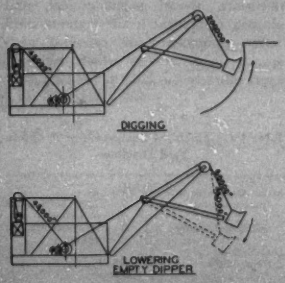


Figure 7. Shovel with balanced hoist shown schematically digging and lowering empty dipper

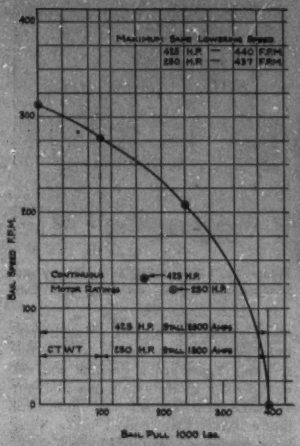


Figure 8. Speed-pull curve of large shovel hoist comparing large motors without balanced hoist and small motors with balanced hoist

have shovels and draglines on their lines install, I think unfairly, ratchets on their kilowatt-hour meters. During periods of regeneration, of course, the shovel tends to drive the kilowatt-hour meter backward and unwinds some of the power recorded previously. Ratchets prevent this. These large stripping shovels operate three shifts per day whereas coal is usually loaded out of the mine during only one shift. During the day shift, of course, there is enough other load on the line inside the meter to absorb the regenerated power from the stripping shovel, but during the night shifts this is not usually the case. Hence the energy shown by the kilowatt-hour meter for large stripping shovels, with or without a counterweight, is greater than the energy actually used on a net basis. The difference between actual and recorded energy is less with a balanced hoist shovel, because the balanced hoist stores up a certain amount of the energy during the lowering of the dipper and gives it up during the digging. This is another reason why a balanced hoist shovel has a lower power bill, and is in addition to the

Figure 9. Large stripping shovel with balanced hoist stripping bituminous coal

fact that its motors being smaller to begin with use less power.

Another advantage is faster acceleration resulting from smaller motors.

A comparison of flywheel energy of motor armatures typical of those used on 35- and 40-yard shovels today is helpful. The 425-horsepower motor is used without a counterweight, and the 250-horsepower motor with a counterweight, two of each driving the hoist motion. The relative figures for one well-known make of motor are shown in Table II.

The older 250-horsepower motor shows a considerable advantage in flywheel effect, but the new 250-horsepower motor available today is still better as indicated by the figures. This explains why shovel runners notice that the smaller motors start and stop quicker, resulting in a faster digging cycle.

A comparison of bail-pull and bail-speed figures of two 425-hp motors without balanced hoist and two 250-hp motors with balanced hoist shows that the two can be made to do the same work and the smaller motors have the advantage of faster acceleration, reduced power consumption, and reduced heating. In Figure 8 the continuous rating of the 425-horsepower motor without balanced hoist occurs at a lower bail pull than in the case of the smaller motor. That is to say, a greater percentage of the cycle with the large motors exceeds the continuous rating.

It is true that the counterweight carries a rope maintenance item, but estimates

Table II

Motor Frame	Horsepower Continuous	WR^2	Continuous Rating, Rpm	Maximum Safe, Rpm	$WR^2 \times$ Continuous Rpm²	$WR^2 \times$ Maximum Rpm²
422	425	1,800	375	1,250	253 $\times 10^6$	2,820 $\times 10^6$
420	250	800	400	1,400	128 $\times 10^6$	1,578 $\times 10^6$
618	250	580	410	1,500	97.5 $\times 10^6$	1,300 $\times 10^6$

Figure 10. Large dragline stripping iron ore in Minnesota, and loading a belt conveyor through a mobile hopper

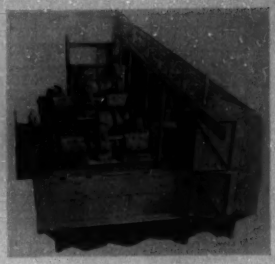


Figure 11. Large dragline machinery with independent motors on drag and hoist

show that this is less than the cost of the extra power with the larger motors. Figure 9 shows a large shovel with balanced hoist stripping coal.

The balanced hoist is available only on the larger stripping shovels. The smaller quarry and mining type machines do not have enough space to permit such an installation.

Large Ward-Leonard Draglines

The advantages of Ward-Leonard control are not confined to the shovel. They are also effective on the dragline excavator. With a dragline the digging bucket is suspended on two ropes, the drag rope which does the digging, and the hoist rope which does the lifting after the bucket is filled. A dragline stands above its work and digs below itself, sometimes being used to excavate under water. In contrast to this a shovel digs above itself. A dragline has the advantage of longer reach and has in recent years become very popular for coal stripping where properties with shallow overburden are becoming scarcer. Deeper stripping is being considered today in the order of 75 feet or more cover above a vein of coal. As draglines become larger, the conventional method of handling the bucket by driving the drag and hoist drums with one motor and using clutches and brakes becomes more of a problem.

During 1942, the first dragline with individual motors on the drag and hoist drums was put into operation with a 25 cubic yard bucket in an Illinois coal strip mine. This eliminated the need for using brakes and clutches, and while such a drive is quite expensive, the benefits are so great that the trend has now spread to smaller machines, even down to the eight cubic yard size.

Figure 10 shows a 25 cubic yard dragline with individual motors stripping iron ore in Minnesota. Large draglines are widely used in the anthracite field where there is much rock digging and such draglines perform exceptionally well in this service. Years ago a dragline was not considered a good rock digging tool, but Ward-Leonard control and individual motor drive have helped make these large machines economical in rock work.

These machines handle 25 cubic yard buckets on 180-foot booms and swing on a 34 foot diameter roller circle. Such a machine of course is shipped knocked down and consists of 47 freight car loads. The working weight is approximately 2,500,000 pounds including 275,000 pounds of ballast in the rear of the revolving frame. It is equipped with walking-traction mounting instead of crawlers as commonly used on shovels. It is driven by two-motor-generator sets and d-c motors as shown in Table IV. Figure 11 shows the machinery layout of a large dragline.

A dragline of this size will draw peak loads of 2,600 kw and pump back to the line peaks of about 1,300 kw. The energy consumption, in kilowatt-hours per cubic yard excavated, is 50 to 75 per cent greater than that of the large shovels equipped with balanced hoists. The dragline usually moves the material farther and lifts it higher than the shovel and considerably more electric equipment with attendant losses is required for the dragline to handle the same yardage.

The dragline does not lend itself well to the benefits of a live counterweight. On a shovel, one motion digs and hoists and one counterweight assists both operations, whereas on the dragline, two counterweights would be required with resulting complication. Furthermore, the dragline ropes travel farther than the shovel ropes so that either excessively high counterweight towers would be required, or one must accept higher friction losses and counterweight rope wear by using more parts of line and more sheaves with short towers.

An alternate power peak absorbing arrangement as used in large steel mill drives and mine hoists, consisting of flywheels in the motor-generator sets, has not yet seemed justified.

Table III. Comparison of Shovel Dipper Power With and Without Balanced Hoist

Motors	2—425 Horsepower Without Balanced Hoist	2—250 Horsepower With Balanced Hoist
Stalling Bail Pull, Pounds		
By motors	375,000	275,000
By counterweight		100,000
Total	375,000	375,000
Maximum Safe Lowering Speed		
Motor—rpm	1,250	1,500
Dipper bail—fpm	440	437
Continuous Motor Rating Bail Pull, Pounds		
By motors	171,000	119,700
By counterweight		100,000
Total	171,000	219,700

Temperature Compensation

The early Ward-Leonard equipment on shovels and draglines experienced an excess of bail pull and motor torque when starting up cold in the wintertime. This came about because at low temperatures, field resistances were low and excitation above normal on the exciter, generator, and motor. After the equipment had been running for several hours and was up to temperature, motor torques would become normal. In northern climates this differential was a considerable item, and combined with it was the fact that most steels are weaker at low temperatures, and should not be subjected to higher than normal stresses. In some localities, greater than normal dipper handle maintenance was experienced in the wintertime.

This problem was well considered by operators, shovel manufacturers, and electric equipment manufacturers, and today equipments are provided with special characteristics which maintain approximately the same motor torques at

Table IV. Electrical Equipment of 25 Cubic Yard Dragline

Motor-Generator Sets	
Hoist and drag	1—1,250 horsepower synchronous
Swing and exciter	1—500 horsepower induction
D-C Motors	
Hoist motor	2—425 horsepower continuous
Drag motors	2—423 horsepower continuous
Swing motors	3—125 horsepower continuous

various temperatures. For example, starting up on a cold morning, exciter voltages are below the normal of 125 volts.

One scheme of accomplishing this result is to use a high resistance differential shunt field in the constant voltage exciter. Another scheme is to use thermal magnetic shunts between the main pole tips in the constant voltage exciter. Both schemes are effective.

Rotating Control

Large draglines and large shovels are equipped today with what may generally be termed "rotating control" in connection with the Ward-Leonard application. Commonly used on excavators are the General Electric Company "Amplidyne" and the Westinghouse Electric Corporation "Rototrol". Rotating control adds a variable voltage exciter, in fact, a small motor-generator set to each of the hoist, swing and crowd motions in addition to the constant voltage exciter, which is common to all motions. These rotating control units replace the bulk of the contactors and part of the field resistors required with the magnetic-type control used on the smaller machines. Rotating control produces a faster response of the generators to the needs of the operator for quick changes in power, with the result of an appreciable gain in output. The advantages, however, do not seem to justify the extra cost on shovels smaller than five or six cubic yards, and draglines smaller than eight cubic yards.

This brings us up to date on the subject of Ward-Leonard control for electric excavators, and in closing I am tempted to forecast the future trends which, of course, is a dangerous pastime. However, there does not seem to be any other scheme of power transmission on the horizon which is as well suited to the requirements of shovel and dragline operation as the modern Ward-Leonard electric control. The nearest approach today is the hydraulic transmission with variable stroke pumps, the "Oilgear", being a good example. However, such equipment is not yet developed in large enough units, and even in the small sizes available today, it is still too costly, in comparison with Ward-Leonard equipment, to be used commercially on shovels.

High pressure steam and enclosed circuit compressed air systems have been proposed, but the shovel industry has not been large enough to carry on such pioneering work itself. The trends in locomotive development include electric transmission with steam and gas turbine drives as well as with Diesel engines; hence the shovel industry does not expect much help from this quarter as far as transmission of power from a constantly rotating prime mover to the shovel dipper is concerned.

For contracting work, the clutch-type machine driven by internal combustion engines is preferred because of its independence of power lines. No doubt when higher-speed more compact Diesel engines are available, Diesel drive will be used on larger machines, combined probably with Ward-Leonard transmission. The friction driven machine is gaining ground in the smaller sizes, as evidenced by the fact that before 1930 Ward-Leonard electric shovels were sold in sizes as small as $1^1/_2$ cubic yard capacity, whereas today there are very few machines sold smaller than $2^1/_2$ cubic yard capacity.

It seems quite certain that any form of power transmission which is to replace the Ward-Leonard drive for large machines in the future must provide for power braking, as it has been a great relief to both user and manufacturer to be free of the problems connected with large heavy duty mechanical brakes and clutches. Ward-Leonard control with its regenerative braking characteristic has done the job so well that it seems to be safely entrenched for some years to come.

No Discussion

Electric Equipment for Power Shovels and Draglines

W. H. SCHWEDES
ASSOCIATE AIEE

D. STOETZEL
MEMBER AIEE

THE rapid mechanization of all industry is perhaps nowhere better typified than in the mining of basic raw materials from the earth. These raw materials, coal, metals, minerals, and construction materials, occur either at places on or near the surface of the earth, or deep within the earth.

This paper is primarily concerned with surface excavating apparatus that is predominant in the production of these raw materials. Power shovels and draglines today handle more tons of earth and its mineral products than any other form of earth-moving apparatus, and last year they moved close to five billion tons of material, or about 70 per cent of all material handled. This included approximately 78 per cent of the three billion tons of coal, iron, copper, lead, zinc, and other metals and minerals mined, including earth moved prior to mining, approximately 95 per cent of the 650 million tons of construction materials, sand, gravel, stone, and clay produced, and approximately 60 per cent of the two and one-half to three billion tons of earth moved in construction operations. An idea of the magnitude of raw materials production today can be obtained by comparing these tonnages with a well known excavation like the Panama Canal, whose development through 1920 amounted to about 400 million tons.

Description of a Shovel

A power shovel or dragline is a production tool whose output is measured in terms of the amount of material that can be handled, the distance the material can be moved, and the time consumed in normal operation. The physical size of a machine is determined by these factors, and is usually identified in terms of dipper or bucket capacity in cubic yards and reach or boom length in feet. For a given machine these dimensions may vary with weight per cubic yard of material being handled. The larger shovels and draglines are the largest mobile production tools in use today.

A modern power shovel consists essentially of six parts. A typical large stripping shovel is illustrated by Figure 1. The dipper handles the material by a scooping action from the hoist motion into the open end of the dipper and subsequent discharge through the opposite or door end. The scooping is augmented by teeth set along the lower open lip. The discharge door is tripped to unload the material at the will of the shovel operator. The dipper stick mounts the dipper and causes its extension into or withdrawal from the work due to effort from the shovel crowd or thrust motion. The boom usually mounts the dipper stick and often the crowd machinery and extends further out into space to mount a sheave over which the hoist rope passes to lift the dipper. The length of the boom determines the reach of the shovel. The shovel shown in Figure 1 has a slightly different crowd motion arrangement to perform the same function. The upper frame carries the lower end of the boom and, by suitable supporting and bracing, fixes the boom position in space so that the boom and upper frame are integral. The upper frame is covered and mounts the hoist, swing, and power conversion or engine machinery, and occasionally the crowd machinery. The upper frame is rotated about a center pintle on rollers on a circular track by the swing motion. The lower frame supports the swing circle and hence the complete shovel. The lower frame is supported by caterpillars and the propel motion is accomplished by suitable machinery at the caterpillars or by mechanical connection to one of the main shovel motions.

Description of a Dragline

A modern power dragline consists essentially of the same parts as a shovel, with a few exceptions. A typical large stripping dragline is illustrated by Figure 2. The bucket handles the material by a scooping action from the drag motion, the material being subsequently discharged out the same open end of the bucket by a tipping action from the hoist motion. The front or open end of the bucket is equipped with teeth along the bottom lip to augment scooping, and the attitude of these teeth to the work is maintained by the hoist motion to control the amount of scooping. A dragline has no dipper stick or crowd motion. The boom can then be much longer than that on a shovel. The upper frame mounts the boom and main machinery, as on a shovel, and similarly, rotates on a swing motion circle. The lower frame carries the dragline weight and may be supported by caterpillars or a flat tub. If equipped with caterpillars, the propel motion is similar to that of a shovel. If the dragline is of the walking type, the propel motion serves to lift and move the lower frame tub by means of large outboard shoes hung from and actuated by machinery in the upper frame.

Shovel and Dragline Application

Excavators are applied to a given job as a function of the daily or monthly output requirements, the type and structure

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W. H. Schwedes and D. Stoetzel are with the industrial engineering divisions of the General Electric Company, Schenectady, N. Y.

Figure 1. Large power shovel with 40 cubic yard dipper, electric d-c Ward Leonard drives, and Amplidyne control

Figure 2. Large power dragline with 25 cubic yard bucket, electric d-c Ward Leonard drives, and Amplidyne control

of the material to be produced or moved, whether the material is to be loaded in haulage equipment or merely displaced, and the range or economic duration of the job. These factors with minor variations influence the selection by the user and his choice between shovel and dragline. This paper does not intend to cover this field. However, a general utility tabulation by shovel and dragline dipper or bucket size is shown for illustrative purposes in Table I. Note the predominant production part played by large stripping shovels and draglines, accounting for their prestige in the industry.

Shovel Motion Requirements

The hoist motion speed-torque requirements are indicated in Figure 3(a). High torque, adjustable by the operator to meet the load demand, with a definite maximum limit, is required from slow through intermediate speeds, with high torque available for acceleration, deceleration, and reversing. The system must withstand heavy shock loading. High speed, adjustable by the operator, must be obtained for low-torque requirements. When lowering, speed should be even greater for economy of time, as indicated in Figure 3(b), since torque requirements are lower.

The crowd motion speed-torque requirements are similar to those for the hoist, as in Figure 3(a), with both speed and torque adjustable. The crowd system must also withstand heavy shock loading. Speed changing, however, is not required between the two directions of motion.

The swing motion speed-torque requirements are indicated in Figure 3(c). Again adjustable torque is required, with high torques available for acceleration, deceleration, and reversal. However, the rate of application of torque to the drive should be low enough so that the high inertia swing system will not experience excessive mechanical stress in its parts. The low-torque speeds should be as high as possible for coasting.

The propel motion speed-torque characteristics should be similar to those shown in Figure 3(a). The main requirements are adjustable torque for starting, with high maximum torque available, and constant speed running.

Dragline Motion Requirements

The drag motion on a dragline is essentially the work motion. The drag speed-torque requirements are shown in Figure 3(a), as for a shovel hoist, with both speed and torque adjustable. Again this system must have high shock load capacity. For economy of time when paying out the drag rope, speed should be increased over the drag speed, as in Figure 3(b).

The hoist motion on a dragline is more of an accessory to the drag motion. Therefore, particularly when dragging up a bank, the hoist motion speed-torque characteristics should have steep slopes so that the hoist will tend to automatically follow the drag to facilitate even, consistent loading of the bucket. The requirements are therefore as shown in Figure 3(c), with adjustable high torques and steep slopes. For economy of time when lowering, the speed should be increased over the hoisting speed, as in Figure 3(d).

The swing motion requirements are as shown in Figure 3(c), with adjustable torque and high-speed coasting. As for a shovel swing, emphasis is on smooth, even application of high torque for acceleration and deceleration.

The propel motion requirements are again the same as indicated in Figure 3(a), with adjustable torque for starting and

Table I. Application of Power Shovels and Draglines and Their Power Drives

Application of Power Shovels and Draglines		Sizes, Average Cubic Yard Capacity					
Utility		1/2–1 1/2	2–3	3 1/2–6 1/2	7–10	11–40
Sand and gravel pits		x	x			
General construction		x	x	x		
Coal, iron, metal and quarry loading			x	x		x
Coal, iron, metal and quarry stripping				x	x	
Heavy mine stripping						x
Large construction					x	x
Application of Power Drives						
Source	Drive					
Engine	Clutch	x	x	x		
Engine	Clutch and electric, d-c (Ward-Leonard)			x	x	
Engine	Electric—d-c (rheostatic)		x	x		
Engine	Electric—d-c (Ward-Leonard)			x		
A-c system	Clutch	x	x	x		
A-c system	Clutch and Electric, d-c (Ward-Leonard)			x		
A-c system	Electric, d-c (rheostatic)		x	x		
A-c system	Electric, d-c (Ward-Leonard)		x	x	x	x
Approximate Production by Size*		10	25	20	20	25

* Per cent of total material handled yearly by shovels and draglines.

constant speed running. Very often the propel drive is common to one of the main motions, so that mechanical means provide necessary lower speeds and higher torques than are normal for that main motion.

Power Drives for Shovel and Dragline Motions

The power drives employed on shovels and draglines to achieve the functional motion requirements have evolved with growth of the industry from steam to the modern d-c Ward Leonard drive with generator field control. Early attempts at electrification were aimed at reproducing the characteristics of the steam drive. Later efforts have improved on them by obtaining far greater digging ability, more protection for machine and personnel, lower operating costs and maintenance.

Two different systems of generator field control for electric d-c Ward Leonard drives are used today. They are known to the trade and will herein be referred to as the magnetic type employing contactors or other magnetic switching devices to adjust the generator field, and the rotating type, employing a separate exciter for supplying and adjusting the generator field.

An evaluation of the requirements for best performance from a motion drive is shown in Table II, comparing earlier with modern systems. Speed-torque requirements for best motion performance already have been indicated in Figure 3. Speed-torque characteristics available from the six systems, shown in Figure 4, are compared on an equal horsepower and maximum torque basis, except that the clutch and electric a-c rheostatic drives require considerably higher pull-out torques.

Table II. Comparison of Various Motion Power Drives

Requirements for Best Performance	Steam	Clutch	Electric-A-C (Rheostatic Control)	Electric-D-C (Rheostatic Control)	Electric-D-C Ward Leonard, (Magnetic Control)	Electric-D-C Ward Leonard, (Rotating Control)
High power capacity	Low	Low	Fair	Fair	High	High
High momentary overload capacity	None	Low	Fair	Fair	High	High
High power efficiency	Low	Fair	Fair	Fair	High	High
Protection from extreme shock loading	Good	Poor	Poor	Poor	Good	Good
Reversing drives all motions	Yes	Yes	Yes	Yes	Yes	Yes
Regenerative braking all motions	No	No	No	No	Yes	Yes
Adjustable torque control	No	Yes	Yes	Yes	Yes	Yes
Adjustable rate of torque application	No	No	No	No	No	Yes
Adjustable speed control	No	No	No	No	No	Yes
Automatic speed changing (where desired)	No	No	No	Yes	Yes	Yes
Coordination of motion controls	Poor	Poor	Poor	Poor	Fair	Good
Minimum operator fatigue	No	No	No	No	No	Yes
Power-temperature stability	Fair	Poor	Fair	Fair	Good	Good
Control stability	Good	Fair	Good	Good	Good	Fair
Sturdy design and construction	Yes	No	No	Yes	Yes	Yes
Low space and weight requirements	High	Low	Fair	Fair	Low	Low
Low inertia of rotating machinery	Low	Low	High	Low	Low	Low
Simplicity of machinery and control	Great	Great	Fair	Fair	Fair	Fair
Low maintenance of machinery and control	High	High	High	Fair	Low	Low

Steam Drives

The steam drive was considered good because high torque was available at low speed, and the drive speed was high for low-torque requirements. The steam engine had low inertia for rapid speed changes, and was simple, sturdy, and rugged. However, the steam drive lacked high peak power output. Power efficiency was low, with problems attendant to coal and water supply. Speed braking was provided. Space and weight requirements were excessive and maintenance high. Because of these many disadvantages, steam drives have been universally superseded by electric and internal combustion engine drives.

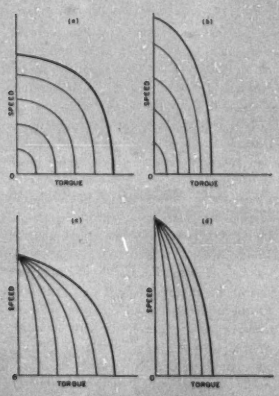

Figure 3. Speed-torque requirements for shovel and dragline motions

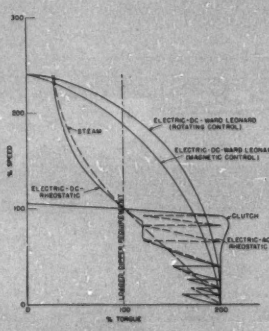

Figure 4. Speed-torque characteristics available from various types of shovel and dragline power drives

Clutch Drives

The clutch drive is simple, inexpensive, has low inertia for rapid speed changes, and space and weight requirements are low. However, the clutch drive power characteristics are dependent on those of its prime mover. To obtain high-torque output comparable to other systems, the prime mover must have much higher maximum torque. To obtain comparable high speed for a low-torque requirement, the prime mover must be oversize in power rating. The clutch drive is used primarily on small shovels and draglines driven by gas and Diesel engines or a-c motors. On some medium sized draglines, this type of drive is still used because it provides low inertia, high-speed lowering of the hoist or high-speed payout of the drag motions. Maintenance is high enough to discourage usage on the larger excavators.

Magnetic couplings are used on some small and medium shovels and draglines to overcome the maintenance due to wear on usual clutch friction surfaces. This drive provides smooth, adjustable torque control with definite torque limit. By special treatment, adjustable speed control is possible. Like electric a-c rheostatic drives, the magnetic coupling has high-speed low-torque limitations. Clean release, low inertia, and heat dissipating capacity should be provided.

Electric A-C Rheostatic Drives

The electric a-c rheostatic drive (wound rotor motor) was simple and stable and

provided fairly good accelerating torque control, but required load limiting relays. Space and weight requirements were fairly low. However, high-speed low-torque operation could not be obtained without the use of oversize motors. Speed control was poor, with no automatic speed changing, and good motion co-ordination was difficult to obtain. Power efficiency was low. Machine inertia was high. Maintenance was high due to difficulties in producing sturdy designs. This drive was one of the first attempts to simulate basic steam engine characteristics electrically, but is obsolete today.

Electric D-C Rheostatic Drives

The electric d-c rheostatic drive provides fairly good torque control, high light-load speeds, and automatic speed changing. Machine inertia is low and mechanical construction is sturdy to afford low maintenance. Space and weight requirements are low. However, power efficiency is also fairly low and no positive speed control is provided, resulting in poor motion co-ordination. Of the early attempts to electrify drives, this type offered the closest to steam characteristics. Where inexpensive, very small drives are required, this type is still used today.

Electric D-C Ward Leonard Drives

The electric d-c Ward Leonard drive with magnetic generator field control affords high peak-power at high speed from low thermal machine ratings. In ability to handle shock loading, the drive is second only to the steam engine. Power efficiency is higher than in other systems, considering very little full-speed operation in a cycle. Good, adjustable torque control is provided, with adjustable regenerative braking. Counter-torque braking is not required. Automatic speed changing is accomplished by steep generator characteristics and motor field weakening. Power characteristics are made stable with respect to temperature variations. Machine design is sturdy and simple and motor inertia is low, providing low space and weight requirements and low maintenance. Even though adjustment of light-load speed over a wide range is lacking with magnetic generator field control, the drive can be used to power even the largest shovels and draglines. Very good operating results are obtained under the severest of duty requirements. Today the drive is used extensively, particularly on smaller shovels and draglines where the refinements and special operating characteristics of rotating generator field control are not economically warranted. The drive is also used quite often for the swing motion only on smaller clutch drive machines.

The electric d-c Ward Leonard drive with rotating generator field control affords all of the foregoing advantages plus very flexible adjustments of speed and torque control. Adjustable rate of application of torque (dynamic response) is afforded. This control can be designed to fulfill the speed-torque requirements for any shovel or dragline motion for proper co-ordination of motion controls and minimum operator fatigue. This type of drive with rotating generator field control is now standard on all large shovels and draglines.

Components of Typical Electric D-C Ward Leonard Drive

This shovel or dragline power drive system is of the adjustable d-c voltage type, comprised essentially of a d-c motion-driving motor which receives power at adjustable voltage from a d-c generator. The generator is driven at constant speed by and receives power from an a-c motor or occasionally an engine. Generator field excitation is derived from a constant voltage source and is adjusted by the control circuits. On larger excavators several drive motors per motion may be required, each receiving power from its own d-c generator.

D-C Driving Motors

D-c driving motors are built for extra-heavy-duty service to withstand severe mechanical strains, variable voltages, and heavy currents. They are the same rugged motors used so successfully in steel mills for auxiliary and crane service. Heavy steel frames are split and the top half hinged for ease of inspection in typical motors. Mounting dimensions are standardized throughout the industry. Large roller bearings on horizontal motors and large ball bearings on vertical swing motors are mounted in dust-tight, removable, keyed frame heads to carry the heavy shock loading. Therefore, armatures can be removed easily without exposing bearings to dust and dirt. Armatures are especially designed for low inertia and high speed operation. Long life brushes, extra large commutators, and high insulation levels provide extra overload capacity. These protected motors are provided with high-temperature, class "B" insulation with inorganic, silicone-type binders, and are separately forced ventilated to obtain the highest power capacity for low space and weight requirements. Motor driven blowers are usually integrally mounted. Some progress has been made toward a filtered air supply common to several motors. A complete line of these 230-volt d-c, shunt wound, blown, 75 centigrade rise continuous motors is available, ranging in size from $12^1/_2$ horsepower at 775 rpm up to 600 horsepower at 375 rpm. Smaller protected, self-ventilated motors are also available for continuous operation at 75 centigrade rise. While these motors are nominally rated at 230 volts, d-c, they are selected to obtain maximum power for the drive and to operate at peak voltages of 600 volts and peak currents of 200 per cent. Figure 5 illustrates the rugged, split-steel-frame construction of a medium size motor, with integral bearing heads and broad, sturdy mounting feet. Table III indicates the application of drive motors to several typical sizes of shovels and draglines.

Figure 5. Typical heavy duty, armored, d-c, motion-drive motor with integrally mounted ventilating blower

Motor-Generator Sets

The motor-generator sets for shovel and dragline service are also especially designed for extra-heavy duty, capable of withstanding the shocks and tilt of excavator movement and the high voltages and heavy loads incident to drive motor demand. Figure 6 shows a typical motor-generator set for a large shovel. The set is carried on a heavy, rigid, self-supporting steel base with mounting pads.

D-c generator frames are split for ease of inspection. Solid steel couplings, con-

necting the units on a set, are provided with jacking screws to permit removal of one armature at a time. Each coupling is taper fitted to its shaft to facilitate bearing removal. Typically, each generator is equipped with one large ball bearing, cradle mounted in heavy brackets which are welded securely to the lower half of the generator frame for small and medium excavators, and pedestal mounted for larger equipment. The smaller motor-generator set induction driving motors are equipped with two end-shield type ball bearings. Medium sized synchronous driving motors are equipped with two large end-shield type roller bearings plus a deep groove ball bearing to withstand axial thrust of the set. For the larger equipment, the synchronous driving motors have two pedestal-mounted ball bearings, withstanding both radial and axial loads. Antifriction bearings afford a minimum of maintenance and enable the sets to withstand a higher degree of tilt incident to operation on a grade and motion during propel.

D-c generator armatures are insulated, braced, and banded for the high voltages and currents of this severe duty. Class "A" insulation is entirely adequate for this service. Large commutators must be well seasoned during manufacture to afford long life, low brush wear, and black commutation at high current levels. Generator ratings are selected roughly for the same full load current rating as the motion drive motor, with the kilowatt rating approximately 0.9 of the motor horsepower rating. Generators are available over the range of 21 kw at 1,800 rpm to 375 kw at 1,200 rpm, rated open, 50 centigrade rise, continuous, 250 volts, direct current. Due to the high peak power service they are also constructed for operation at 600 volt and 200 per cent current peaks. Fields are differentially compound wound.

Induction motor-generator set driving motors are provided with special stator winding and end turn bracing due to shock loadings and frequent use of full voltage starting. Class "A" insulation is used. Open, 40 centigrade rise, continuous ratings are available at 2,200 or 3,810 volts from 125 to 500 horsepower at 1,800 rpm. Two hundred and fifty per cent maximum running torque is usually provided.

Figure 6. Typical large shovel or dragline power conversion motor-generator set, showing two hoist, one crowd, and two swing motion generators, constant voltage exciter, and 900 horsepower, 1,800 rpm synchronous driving motor

Synchronous motor-generator set driving motors are designed usually for 225 per cent pull-out torque and special field winding bracing. Class "A" insulation again is used. Open, 0.8 power factor, 40 centigrade rise, continuous ratings are available at 2,200 or 3,810 volts from 450 horsepower at 1,800 rpm to 1,250 horsepower at 1,200 rpm.

Constant voltage exciters for excitation of motion drive motor shunt fields, synchronous motor shunt fields, generator shunt fields, and control are accumulatively compound wound, usually with a flat voltage characteristic from approximately 50 per cent to 125 per cent load. Thermal-magnetic exciter field shunts or other means are provided to compensate for loss in motion power due to ambient and machine temperature rise. Otherwise temperature rise would cause increased exciter, generator, and d-c motor field resistance, and hence, decreased excitation, output, and excavator torque and power. The operating temperature range on an excavator may well vary from −25 to +75 centigrade. Class "A" insulation is used. Open, 50 centigrade rise, continuous ratings are available at 125/125 volts, direct current, from 6 kw to 40 kw at 1,800 rpm.

Table III. Typical Shovel and Dragline Motor Applications

Shovel Dipper Capacity, Average Cubic Yards	2½	4	6½	18	40
Hoist motion, hp	75	125	250	2–187½	2–425
Crowd motion, hp	23	37½	50	75	2–75
Swing motion, hp	23	37½	2–40	2–75	3–125
Dragline Bucket Capacity, Average Cubic Yards	2½	4	9	15	25
Hoist motion, hp	75	125	325	2–250	2–425
Drag motion, hp			325	2–250	2–425
Swing motion, hp	23	37½	2–75	3–75	3–125

Control

Control for adjustable voltage, Ward Leonard drives function to adjust the generator output to the will of the operator or to protect the system from excessive voltage and current. Two basic control systems for adjusting generator voltage output are in use today, magnetic and rotating. Table II compares the two in respect to optimum performance.

Magnetic Generator Field Control

Magnetic control consists of the elements required to adjust a generator shunt field, supplied from the constant voltage exciter, by adding or subtracting series resistance in response to the operator's master switch. Figure 7 sketches a typical magnetic generator field control circuit. Current carrying capacity of master switches is low. Therefore, contactors are used as amplifiers to do the actual work. On high inertia swing motions extra relays, not shown, may be used to prevent too-rapid application of torque by adding time delay between the closing of the accelerating contactors.

In addition to the separately excited generator shunt field thus controlled, a series differential field is wound on the generator main poles to provide sufficient compounding to collapse or "stall" the system at excessive loadings, and hence limit current and torque to approximately 200 per cent. With the stall conditions thus obtained and with no-load generator voltage limited to about 600 volts by the saturation characteristics of the generator, then the intermediate volt-ampere characteristic of the generator, and hence speed-torque characteristic of the motion drive motor, is determined by the amount the generator poles are saturated at no-load. No saturation at no-load would produce a straight-line compounding curve to stall as indicated by the dotted curve in Figure 8. Heavy saturation would produce nonlinear compounding as shown by the heavy curve in Figure 8, to afford high peak power for

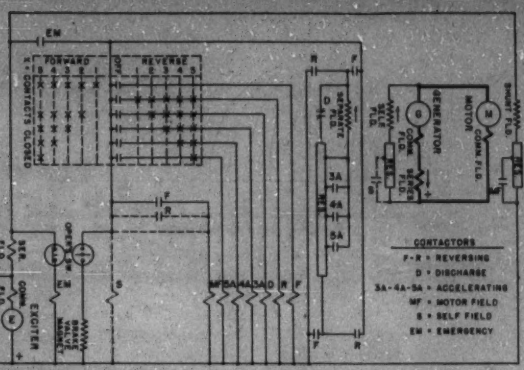

Figure 7. Functional circuit schematic of a typical electric d-c Ward Leonard drive with magnetic generator field control

the drive, but would require excessive generator iron and separate excitation and would result in sluggish protective response to shock loads.

Therefore, to augment moderate no-load saturation, the generator main poles are wound with a self-excited, accumulative, voltage field to obtain the desired peak power. The amount of self-excitation, adjusted by resistors, also determined the rate at which regenerative braking can be applied, thereby affording some reduction in the normal, excessive generator currents obtained when "plugging," or suddenly applying full reverse effort to brake from a high speed. When heavy self-field is used, as on a swing motion control, a contactor is often added to weaken the field for rapid voltage decay in emergency.

Since generator saturation and self-field action are employed to obtain high-power speed-torque characteristics, adjustable speed control over a wide range is prevented. Good torque control, however, is provided, as shown in Figure 8.

The shunt fields of the d-c motion drive motors are weakened by inserting series resistance with master-switch-operated contactors to change the motion speed in one direction over the other or to reduce motor heating at rest. On a typical shovel, for example, the hoist motor fields are weakened during lowering and rest, the crowd motor fields during rest only, and the swing motor fields not at all, due to possible resultant generator current transients peculiar to operation of that motion. Dragline hoist and drag motions are controlled like a shovel hoist motion in this respect.

An emergency contactor can be opened at will or in response to motor-generator set overspeed, loss of a-c power, or other protective signals to remove excitation from the generator system and set the motion brakes. For simplification the protective interlocking is not shown in Figure 7.

Rotating Generator Field Control

Rotating control replaces the exciter source and magnetic contactor adjusting means for the generator separate field, the generator self-field, and part of series differential excitation with a separate, a-c motor-driven exciter. Various exciters and control systems are available, known by the trade names of Amplidyne, Rototrol, Regulex, and others. A typical generator field exciter, known as the Amplidyne, is shown in Figure 9. Small, compact, and made up of standard, well-known d-c machine parts, thousands of applications have proved practical freedom from maintenance. Very high amplification and rate-of-response properties are inherent. Low energy fields can be handled directly from the operator's master switches, and the physical size of resistance and control circuit elements is determined by design for ruggedness and long mechanical life alone. Space requirements are low. Master switch contact interrupting duty and resistor heating are so minimized as to warrant little maintenance.

The high response rate of this generator field exciter, in conjunction with control circuits which cause momentary overvoltage across the generator field during acceleration and retardation, produce forcing or high dynamic response of the motion to the operator's controller. The characteristic compounding curve of the generator can be shaped and realized more closely to obtain more dynamic or peak power and still remain well inside the commutation limits of the d-c generators and motors. Series or generator current differential action is generally applied to both the generator main poles and generator field exciter to force generator voltage collapse under heavy shock loading. The shape of this compounding curve from no-load to stall can be obtained by the use of generator and exciter saturation, augmented by generator or exciter voltage applied to an accumulative exciter field, or by powerful differential generator current compounding applied to the exciter after normal generator load is exceeded, as determined by an adjustable external reference voltage or "bias." The latter arrangement, as shown in Figure 10, is referred

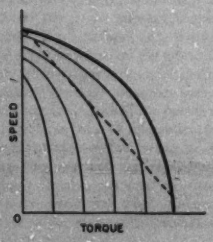

Figure 8 (left). Speed-torque characteristics available from electric d-c Ward Leonard drives, with magnetic generator field control

Figure 9. An Amplidyne unit, typical of a generator field exciter for rotating control

to as the well-known current-limit scheme and provides very high peak power, good shock load response, and adjustable speed control, indicated by the characteristic curves of Figure 11(a). In this scheme (solid lines of the circuit in Figure 10) the generator separate field is excited by the Amplidyne, whose excitation is derived through its control field from the constant voltage exiter and adjusted directly by the operator's controller. When generator current, as measured by the drop across the generator series fields, exceeds a predetermined value, adjusted by the "bias" resistor, the Amplidyne current field provides heavy differential compounding toward stall.

The former scheme, using machine saturation, provides good peak power, adjustable torque control, and good "plugging" control for protection of electrical and mechanical parts of high inertia systems undergoing rapid acceleration or braking. But since saturation is used, it provides no-load voltage limit only and hence very little adjustment of voltage or speed by controller manipulation. Figure 11(b) shows the basic characteristics obtainable from this arrangement. This circuit may be identified from Figure 10 by simply removing the bias resistor and rectifiers in series with the current field and by connecting in the same current field and the additional generator voltage field through the dotted lines shown. The circuit, except for the addition of the Amplidyne, becomes quite similar, then, to the 3-field system described under magnetic control.

Or by a slightly different circuit connection both generator current and generator voltage can be regulated instead of limited, to afford the completely adjustable characteristics shown in Figure 11(c).

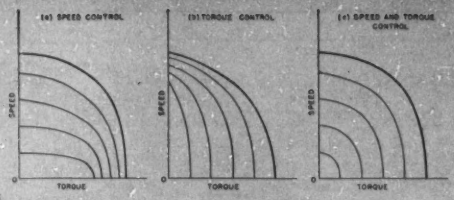

Figure 11. Speed-torque characteristics available from electric d-c Ward Leonard drives, with rotating generator field control

Therefore the control, though simple, is capable of great flexibility of adjustment to meet all the operating requirements of any shovel or dragline motion. Hence the motions on an excavator can be co-ordinated to the best advantage for greatest output and minimum operator fatigue.

Shovel and Dragline Power Sources

A-c power systems supply the majority of power for shovels and draglines in service, and therefore the discussion deals primarily with the requirements and apparatus involved in the distribution of a-c power.

Engines are used as a source of power or prime mover essentially only on smaller excavators. Fuel must be made available at the excavator location. The energy of the fuel must be liberated in an internal combustion engine before mechanical application to the clutch or electric motion drive, and therefore energy reserves and momentary overload capacity are limited. Engines can absorb very little mechanical energy and therefore regenerative braking is not available. Space and weight requirements are usually very high. Therefore, engine sources are used primarily only where a high degree of portability is required, where operations are remote from convenient a-c power systems, or where the temporary nature of the operation does not warrant installation of a-c distribution equipment.

Shovel and Dragline Power Requirements

Energy and power requirements for shovels and for draglines are shown in Figures 12(a) and 12(b), respectively, in relation to dipper and bucket capacity. The values indicated are average for dipper or bucket loading, digging conditions, and excavator builders' design.

Energy requirements are shown in terms of kilowatt-hours per cubic yard of dipper or bucket capacity for average digging conditions and normal movement of the material. Energy consumption will be greater or less as the digging conditions become more severe or easier.

The highest average load for any 15-minute period is called the 15-minute average demand and is measured in kilowatts.

Momentary peak loads during operation are expressed in kilowatts. Total kilovolt-ampere peaks impressed on the power distribution system can then be determined, knowing the power factor of the shovel or dragline load system. Electric shovels and draglines up to a dipper or bucket capacity of eight cubic yards are powered through induction motor drives with average power factors of approximately 80 per cent lagging, and peak power factors approaching 85 per cent lagging. Above eight cubic yard capacity they are powered through synchronous motor drives with average power factors of approximately 90 per

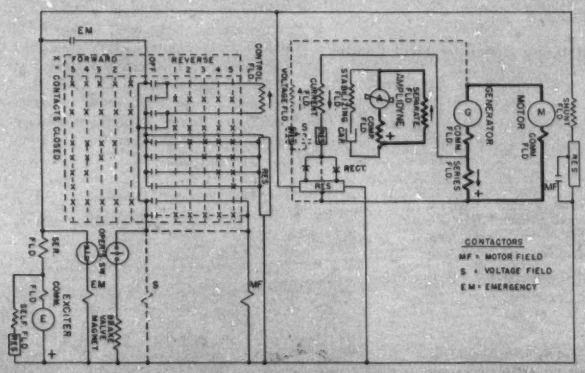

Figure 10. Functional circuit schematic of a typical electric d-c Ward Leonard drive with rotating generator field control

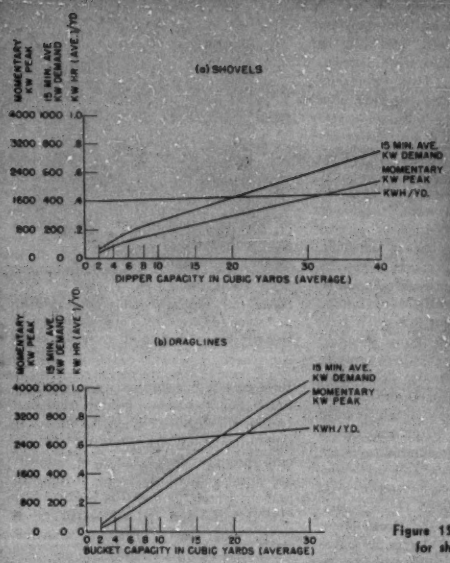

Figure 12. Power requirements for shovels and draglines

Figure 13. Typical package substation for shovel and dragline service, showing switchgear, transformer, and ground protective equipment

cent leading, and peak power factors approaching 97 per cent leading.

A few shovels equipped with counterweighted hoist motion will have 15-minute demand and momentary peaks somewhat lower than shown.

System Voltage Regulation and Power Factor Control

High momentary peak loads must be handled by high-torque motor-generator set driving motors. Since induction motor torque varies with the square of applied voltage and synchronous motor torque directly with applied voltage, it is important that the terminal voltage be maintained as close to rated value as possible. Voltage dips of five per cent will not cause trouble, but dips of 10 per cent may seriously affect continuity of operation.

Where synchronous motors are used, reactive kilovolt-amperes can be added to or subtracted from the power system to stabilize heavy load or light load terminal voltages. This is accomplished by the application of 0.9 power factor synchronous motors whose field excitation can be controlled. Usually a single contactor, actuated by the hoist motion master switch to approximate peak load conditions, serves to cut out resistance in series with the motor field to increase its excitation. Where voltage fluctuations are severe, a stepless synchronous motor field regulator is applied. A typical equipment consists of an Amplidyne, connected buck-boost with the synchronous motor field and constant voltage exciter. The amount of buck-boost is governed by a simple regulator looking at system power factor and load.

System Fault Protection

Particularly on the larger machines with high fixed charges, and with mine output depending on continuity of operation, power system reliability is very important. Outages due to lightning, accidental damage, or line, cable or apparatus fault must be kept to a minimum. High quality distribution apparatus is well worth the investment as insurance alone.

Should machine or system faults occur, the damage and outage must be confined to as small an area as possible. Systems supplying a number of excavators can be arranged easily so that temporary loss of one excavator or its feeder will not shut down any of the others, even momentarily. Therefore, fast, selective tripping of an excavator feeder on line-to-line or line-to-ground fault is provided in modern distribution systems of this nature.

To protect personnel on occurrence of line-to-ground faults, fault current must be limited to a low value so that voltage drops on exposed parts of the ground return path are kept below dangerous levels. A 50 ampere maximum ground current is safe and a convenient level for relaying for fault clearing purposes. The ground-current limit is accomplished by inserting a resistor at the neutral or Y point of the distribution transformer secondary. If the transformer secondary is delta connected, an artificial Y point is established by the use of a zigzag grounding transformer or other means.

Power Distribution Equipment

Since power is usually received at the mine or quarry at transmission voltage levels, a transformer with grounding resistor and protective switchgear must be provided. Power is usually distributed at 4,160 volts, occasionally at 2,400 volts. Cable costs vary almost inversely with the distribution voltage level. In addition, a 4,160-volt grounded system facilitates proper grounding protection.

Substations

A package substation like that shown in Figure 13 provides a co-ordinated, high-quality equipment ready for outdoor installation. This package substation consists of an incoming line structure, a transformer with neutral grounding resistor, and one or more switchgear

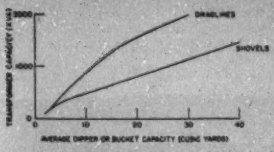

Figure 14 (a). Substation transformer capacities for shovels and draglines

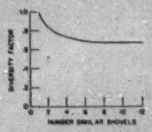

Figure 14(b). Diversity factor

units. The incoming line is provided with disconnects, high voltage fuses, and lightning arresters. The transformer capacity required for various shovels and draglines is shown in Figure 14(a). Diversity factors for the use of more than one similar excavator from the same transformer are indicated in Figure 14(b). The grounding resistor is insulated, protected, and connected to the transformer neutral bushing through a current transformer for relaying and tripping.

The switchgear may provide single or multiple feeder circuits. A transformer secondary circuit breaker is desirable in the latter case. A feeder unit consists of a power circuit breaker with three current transformers and over-current relays to afford instantaneous, selective, line-to-line and line-to-ground fault protection. The same inverse-time relaying protects against cable overload. For back-up line-to-ground fault protection, single-circuit feeder or transformer-secondary circuit breakers are time-delay tripped from the current transformer in the ground return circuit. Power circuit breaker interrupting capacity must meet the short circuit duty of the power system at the transformer secondary. Interrupting levels of 50,000 kva are quite common.

Cable

Power can be distributed from the substation by cable or pole line, determined by working conditions. Cable provides a flexible system since it is portable and can easily be shortened or extended in convenient standard lengths. Lengths of 1,000 to 1,500 feet are commonly used and are easily joined by standard couplers. These cable couplers are safe, light-weight, water-tight, are used on switching units as well, and can be mechanically interlocked with distribution circuit breakers. Two, three, and four cable junctions can be provided at a single coupler.

Cable for distribution and feeder circuits is available in 60 and 75 degrees centigrade temperature ratings. For estimating purposes only, Figure 15 indicates the 3-phase cable conductor size in relation to average or normal excavator dipper or bucket capacity. Counterweighted machines require slightly smaller cable ratings. Figure 15 is based on the use of 75 centigrade rated cable. Cables for lower temperature ratings will be proportionately larger. For very long cables, voltage drop must be considered. Good cable conductor insulation consists of a heat-resistant Neoprene-type jacket, braided with a ground sheath. Cable interstices carry three ground conductors and are filled with jute for flexibility and conductor cushion. Cable protection consists of an inner Neoprene-type cushion-base jacket, a reinforcing twine braid, and a tough Neoprene-type outer jacket that is more than twice as resistant to wear and tear than natural rubber.

Cable Switching Stations

Where shovel and dragline power circuit cables branch off from a main distribution feeder cable, portable switching stations, sometimes called cable skids, are used to provide a cable junction and selective fault protection. Figure 16 shows a typical unit. These cable skids can serve any number of functions in the distribution system. A power circuit breaker of suitable interrupting capacity,

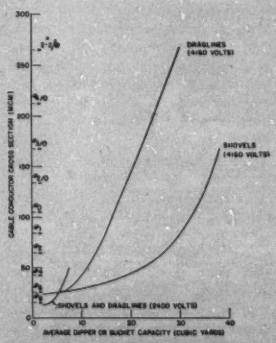

Figure 15. Cable ratings for shovels and draglines

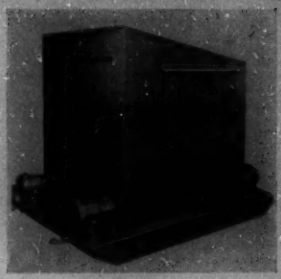

Figure 16. Typical portable cable skid for protection and distribution of power from main feeder cable to individual excavator cables

three current transformers with relays for line-to-line and line-to-ground fault protection, and cable couplers for incoming and outgoing power and several load circuits are provided. All cable couplers are suitably interlocked with switchgear, and the system ground wire is physically extended through and connected to all couplers and skids to protect personnel.

Shovel and Dragline Switchgear

On board the shovel or dragline, switchgear is provided for control of a-c driving motors. At present a maximum interrupting capacity of 50,000 kva is considered sufficient. Usually only undervoltage protection is furnished with this gear, since the distribution system provides overcurrent protection for both machine and cable.

Low Voltage Transformer Stations

Since low voltage power must be provided to service drills and pumps, low voltage portable transformer skids can be used to complete the distribution system. Transformation from the 4,160 or 2,400 distribution voltage to the 460- or 230-volt service, load switching, and line-to-line and line-to-ground fault protection are provided. The equipment is mounted on a portable skid and designed for cable connection to the shovel and dragline distribution system.

Although present surface mine power distribution voltages are limited to 4,160, consideration is being given to the 6,900-volt level, since even larger shovels and draglines are being proposed. The convenience and lower cost of smaller cable and distribution equipment must be weighed against the somewhat greater cost of higher voltage main a-c motors on board the excavator.

Discussion

H. S. Jacobs (Harnischfeger Corporation, Milwaukee, Wis.): Schwedes and Stoetzel have given a most excellent discussion of their subject. There are two points upon which I believe additional information is in order. These are:

Under Clutch Drives the statement is made that the prime mover must have higher torque (this is horsepower at a given rpm) than required for other systems. Our experience is that this is not true where eddy clutches are used. For example, we find that a prime mover producing a peak horsepower output of 340 is sufficient for a four cubic yard shovel equipped with an eddy current clutch on the hoist and Ward Leonard on other motions. The prime mover in this case was a Diesel engine. However, a shovel with Ward Leonard electric drive throughout, we find, requires not less than 30 per cent more peak power, which of course means 30 per cent more torque where comparable speeds are considered. Am I, therefore, correct in assuming that the statement above was meant to apply only to shovels with friction clutches and not to shovels equipped with electric clutches?

The second item is:

We have found where induction control is used that an infinite number of controller steps is available. This enables us to cover the entire area between the step lines shown under Figures 3(a) and (c) as well as by the step lines shown. This gives us satisfactory dipper control under any condition of loading. This is true even though the tendency of the equipment is to respond as shown under Figure 3(c). Is the size of the shovel or the number of controller steps the deciding factor in determining the characteristics required under Figure 3(a) with Amplidyne or 3(c) with magnetic control? If the answer indicates that the shovel size is the determining factor, please state which of your types of control you would recommend for shovels of 6 cubic yards or less in capacity.

W. H. Schwedes and D. Stoetzel: The first point under discussion concerns clutch drives. By definition a clutch (or coupling) transmits torque so that the ratio of driven to driving torque remains constant at unity regardless of speed. A torque converter transmits torque so that the ratio of driven to driving torque increases as driven load torque increases at the expense of driven speed. This is equivalent to a stepless changing of gear ratio. When clutch drives are used, and this applies to friction, electric and hydraulic clutches (except torque converters), the maximum speed and torque ratings of the prime mover determine the available motion drive characteristics. Since the gear ratio of the mechanical connection from a clutch to the actual motion is not changeable, when once determined by the excavator builder, only by the use of high torque, high speed, and hence high power prime mover ratings, can both low speed-high torque and high speed-low torque operation be obtained. Reference to Figure 4 illustrates this point.

On the other hand, since a Ward Leonard drive is a true torque converter, the drive may be powered at a moderate power level with high maximum torque machines, and then high torque is converted automatically to high speed when the load is light. This is an electrical equivalent of stepless gear changing, which cannot be accomplished with a clutch drive.

It is true that a clutch drive with normal power rating can afford either high speed or high torque by proper prior selection of speed level and gear ratio, but not both without extra power capacity of prime mover. A compromise between oversize prime mover and high speed at light load usually results. In addition, where the clutch prime mover is a squirrel-cage induction motor wherein maximum torque drops as the square of a line voltage dip, extra high torque must be provided to meet a heavy stalled load demand.

In a Ward Leonard drive, however, a heavy stalled torque is reflected back as high d-c armature current only, and not as a peak power demand on its prime mover.

The second point under discussion concerns the number of controller steps indicated in the families of speed-torque characteristics shown by Figure 3. It was not the intent of the paper to select the exact number of controller steps to be used. A typical controller with five steps was selected for illustrative purposes. What is important to good control is the relative shape of the family of speed-torque characteristics. Note the differences between the curves of Figures 3(a) and 3(c), wherein the basic outside or limiting characteristics are the same, but the slopes on intermediate controller points differ widely. This means that the rate of automatic gear changing, or torque conversion to speed as load changes, can be altered at will by the operator. With a little practice, the operator soon finds that one of the controller points affords just the right gear changing characteristics, so that he can coordinate that motion with another without continual, tiring manipulation of the controller.

For other motions on the shovel or dragline other families of characteristics are required, as described in the paper. Figure 11 shows that with rotating control of a Ward Leonard drive three widely different characteristic families are available. It is even possible to change from one to another by minor circuit variation only. Each has its place.

Ordinarily the clutch drive affords only one family of characteristics, with the basic limiting characteristic similar to that shown in Figure 4, and the intermediate steps relating to it as in Figure 3(c). Smooth, adjustable torque control only is provided, with the shape or slope of the characteristics determined by prior design.

The Reversing Cycle of Modern Blooming Mills

T. B. MONTGOMERY
MEMBER AIEE

J. F. SELLERS
MEMBER AIEE

WHEN the engineer in the metal processing industry today plans to equip his plant with the latest in rolling mill machinery for reducing ingots to slabs, blooms, or bars, he automatically contemplates the specification of a d-c motor drive with a rotating regulator control. He has available considerable operating experience with this equipment to substantiate his judgment. To completely justify the selection it then becomes highly desirable to be able to predict in advance the results to be expected from a drive of a given horsepower, particularly during the reversing cycle.

By proper preliminary calculation of a modern control and drive system it can be shown that a close approximation of currents, voltages, and torques to be expected during the reversing cycle may be obtained. A valuable means for predicting the reversal time and the reversal characteristics of the drive is thus assured; the proper specification of equipment for obtaining maximum results becoming clearly outlined in the process.

A study is made in this paper of a typical blooming mill with a d-c variable speed motor drive rated at 7,000 horsepower, 40/100 rpm, 700 volts. By comparing the variable speed drive with the equivalent necessary ratings in single-speed motors the variable speed selection can be justified.

Accepting the control complications involved by this selection, calculations are made for the ideal desired reversing cycle. These calculations determine the theoretical excitation, control, and power characteristics necessary to attain desired results. By modifying the theoretical data to suit economically practical equipment a forecast of the anticipated reversing cycle can be made. Field tests substantiate the practical accuracy of the theory.

Characteristics of the Drive

For the rapid reversal of a mass of large WK^2 value, such as that of a blooming or slabbing mill, the application of a drive which would apply constant torque over the complete cycle of full-speed forward to full-speed reverse and vice versa, might, at first sight appear desirable. The torque uniformity and smoothness would be ideal from the mechanical point of view. Furthermore, for the electrical drive of such load a single speed d-c motor could be used with variable voltage control, speed reversal being accomplished entirely by generator voltage. With present-day methods, uniform acceleration with current limit control could be accomplished with relatively simple equipment.

A single speed motor of 7,000 horsepower operated at a fixed field to produce 100 rpm at full voltage would withstand 225 per cent load repeated frequently. With a motor armature WK^2 of 2.15×10^6 and a mill WK^2 of 0.08×10^6, making a total of 2.23×10^6 pounds feet,^{2} 83 per cent rated motor current would be required to reverse the empty mill rolls and motor uniformly in 4.55 seconds as shown by the broken line n'' in Figure 6. Thus approximately 143 per cent rated motor torque would remain for doing work on the ingot.

From the electrical point of view such a drive is inefficient inasmuch as it utilizes only the average or one-half of the peak available horsepower over the entire cycle. Field weakening control utilizes a greater percentage of average horsepower. Also from the standpoint of the job to be done, heavy torques are required for the large reductions in the first few passes when the ingot is short and high speed cannot be utilized. Likewise a margin in torque is required for "cold" ingots and special alloys. The motor under study, 7,000 horsepower with speed range 40/100 rpm giving $2^1/_2$ times the torque per ampere at base speed as at 100 rpm, provides this margin.

A single-speed motor with torque equal to the base speed torque of this motor would require a rating of 17,500 horsepower, with proportionately large generators. Using 75 per cent of full-load current the reversing time of such a single-speed motor is shown by line $n'n'$ in Figure 6, which would cut off approximately 1.5 seconds of the reversing time.

With 40/100 rpm speed range in the motor, with corresponding $2^1/_2$-to-1 range in torque, heavy reduction first passes are adequately taken care of at low speeds with short ingots and ample speed is available after the ingots have been elongated to maintain production schedules. Therefore, this choice is the most economical for the job to be done.

The Ideal Reversing Cycle

The major problem in controlling the drive accurately is occasioned by motor field weakening as the following discussion is intended to demonstrate.

Let us consider that the 7,000-horsepower motor in question has ample capacity to handle repeated loads of 175 per cent frequently at high-speed weak-field setting. If we start the study of the reversing cycle at full-speed forward (100 rpm), the first part of the cycle is, of course, that of strengthening the motor field. If 75 per cent horsepower is allotted to the reversal of the mass, then 100 per cent of rated horsepower remains avail-

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T. B. MONTGOMERY and J. F. SELLERS are both with the Allis-Chalmers Manufacturing Company, Milwaukee, Wis.

able for supplying load on the acceleration part of the cycle. As a consequence, the ideal reversing cycle study is based on reversing an empty mill with armature current maintained constant at 75 per cent. Since the generator voltage is held constant at full value during the motor field weakening and motor field strengthening portions of the cycle, the horsepower acting to decelerate and accelerate the motor will remain constant with 75 per cent rated current flowing.

Equation 8 in the Appendix shows that at this current value the motor will be decelerated from 100 to 40 rpm in 1.63 seconds, which time is based on constant current as expressed in equations 6 and 8.

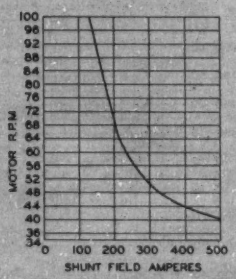

Figure 1. No-load speed curve of a 7,000-horsepower 700-volt 40/100-rpm reversing motor

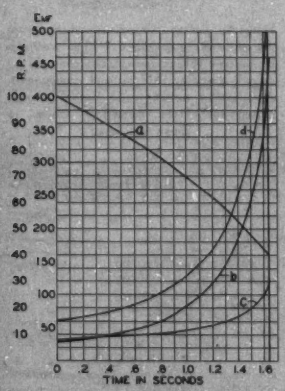

Figure 2. Curve showing a speed change on the motor of Figure 1 from 40 to 100 rpm by field control, depicting the required shunt field voltage components. Accelerating torque is 75 per cent rated horsepower

a—rpm (n) c—i_{MF}
b—L_{MF} di/dt d—e_{MF}

Table I. Motor Speed Change by Field Control With 75 Per Cent Rated Horsepower Accelerating Torque

(1) n_t	(2) $\times 10^{8} = \phi$ (Equation 7)	(3) dt (Equation 6)	(4) Total Elapsed time, t	(5) $\times 10^{8} = d\phi$	(6) 5.28×10^{-8} $d\phi/dt$ (Equation 11)	(7) i_{MF} (Figure 1)	(8) ir_{MF}
100	20.90					132	31.4
		0.1897		1.10	30.6		
95	21.40		0.1897			140	33.8
		0.1800		1.16	34.0		
90	22.56		0.3697			149	35.4
		0.1703		1.34	41.8		
85	23.90		0.5400			158	37.6
		0.1607		1.48	46.6		
80	25.38		0.7007			170	40.5
		0.1510		1.72	60.1		
75	27.10		0.8517			181	43.0
		0.1410		1.90	71.1		
70	29.00		0.9927			195	46.4
		0.1316		2.25	90.3		
65	31.25		1.1243			212	50.5
		0.1218		2.58	111.8		
60	33.83		1.2461			235	55.9
		0.1130		3.07	144.6		
55	36.90		1.3581			263	62.5
		0.1030		3.70	191.0		
50	40.60		1.4601			306	72.7
		0.0925		4.50	256.5		
45	45.10		1.5526			380	90.4
		0.0828		5.65	360.0		
40	50.75		1.6354			508	121.0

Since this requires constant counter electromotive force the field flux must be changed inversely as speed decreases from 100 to 40 rpm. If flux is not increased in the right proportion counter electromotive force and decelerating current may rise sharply to high peak values. High peak currents are to be avoided since they adversely effect commutation and put undue strain on the equipment.

However, the maintenance of constant counter electromotive force during speed change, so as to keep constant current with constant applied voltage, is dependent on the value of inductance L. But the value of L is not constant. In fact, due to the high degree of saturation at full-field current, the quantitative value of L changes widely from minimum-field current to full-field current. High flux density is employed in the motor at base speed to obtain most economical use of material.

Consequently, a step by step method of calculating motor speed and current is necessary since the motor saturation curve cannot be represented by a single equation. The method of calculation is shown in the Appendix for increments of 5 rpm. In Table I, column (3) dt is computed from equation 6 which gives the average for each increment of 5 rpm $(n_0 - n_1)$. The total field voltage required to give a flux rate of change such as to maintain constant counter voltage is plotted as curve d, Figure 2. This curve is the summation of L_{MF} di/dt (curve b) and ir_{MF} (curve c). For both deceleration and acceleration by motor field weakening total field voltage is shown as curve E_{MF}, Figure 6. It is to be noted that at the first instant of deceleration motor field voltage is changed from its steady-state value of ir_{MF} or 31.4 volts to $ir_{MF} + L_{MF}$ di/dt or 62 volts.

In like manner calculations are made in the Appendix for generator field voltage E_{GF} required to produce a rate of change of flux required to maintain 75 per cent decelerating and accelerating current during the portion of the cycle produced by reversal of generator voltage. This curve is shown as E_{GF} in Figure 6.

The IR drop of the armature circuits has not been used in the calculations as there would be slight effect on the curves, and has not been used because the machines change from motor to generator action during reversal.

The Control

The simplified diagram of control to accomplish the foregoing performance is shown in Figure 3. Generators (1), (2) and (3) connected in parallel have interconnected series fields for holding a balance of load between them.

The motor (4) has its excitation supplied by motor exciter (5) which in turn has its excitation supplied by Regulex exciter (6). The generators have their excitation supplied by generator exciter (17) which in turn has its excitation supplied by Regulex exciter (12).

The reference or speed setting fields (13) of the generator Regulex exciter and (8) of motor Regulex exciter are energized from a constant potential source through the mill master. Balance field (9) in

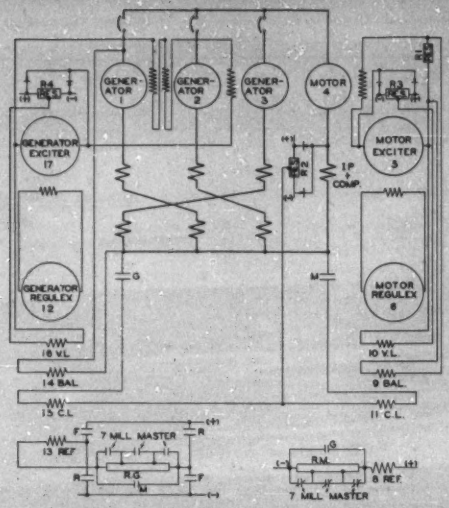

Figure 3. A simplified typical schematic diagram of a reversing blooming mill control

the motor Regulex exciter opposes the reference field (8), being energized from the voltage across resistor "R1," a measure of motor field current. Balance field (14) in the generator Regulex exciter opposing the reference field (13), is energized from the voltage of the generators (1), (2) and (3). Two other fields are supplied on each Regulex exciter, voltage limit fields (10), (16), and current limit fields (11), (15).

The mill master is arranged so that the first steps energize the generator Regulex reference field in the forward or reverse direction, the motor Regulex reference being held meanwhile at its maximum value. The generator voltage rises to balance the Regulex reference, and the next steps of the mill master reduce the motor Regulex reference. This causes the motor field to weaken until the Regulex exciter balances and the motor runs at topspeed, having attained the acceleration first by the rise of generator voltage then by a weakening of the motor field. Deceleration reverses this process.

Control means not shown energize contacts "G" and "M" at the proper point in the cycle so that the correct current limit field is in control. The same means is employed to properly sequence the Regulex reference fields in case of a rapid reversal of the mill master.

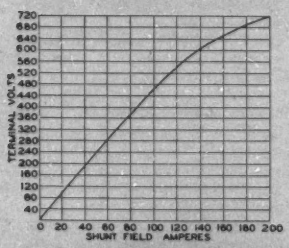

Figure 4. The no-load saturation curve of a generator rated 2,000-kw 700-volt 514/436-rpm at 93 per cent synchronous speed

Predicting the Practical Reversing Cycle

Consideration of Figure 6 makes it evident that the required peak voltage of motor and generator exciters is beyond practical limits for both economical size and rate of response, so a compromise is made by setting a 250-volt nominal rating on the exciters. The flux density is kept low in the exciters to provide fast exciter response and good control. In order that the absence of these peak exciter voltages at and near the point of saturation of the motor and generator fields may not be detrimental to the reversing cycle, by allowing the armature current to be reduced sharply at the points where they are shown in Figure 6, the generator field control is set to overlap motor field control in time. By this means the limits in exciter voltage have no effect on over-all reversing time.

A more important factor encountered in the attempt to set performance to the ideal is the item of time constants. In Figure 6, the curves of required field voltages E_{MF} and E_{GF} change from their steady state value to their transient values instantly. This obviously is not possible in practice.

Since a motor speed change is initiated by a change in the speed setting field of the motor or generator Regulex pilot

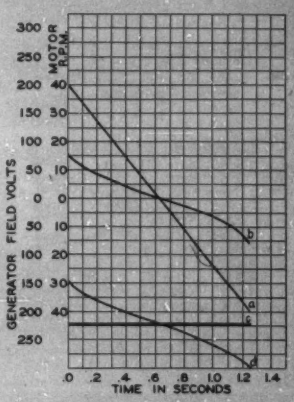

Figure 5. Curves showing a speed change on the motor of Figure 1 from +40 to −40 rpm by variable voltage control, depicting the required generator field voltage components. Accelerating torque is 75 per cent rated horsepower. Three generators similar to Figure 4 are used

a—rpm (n) c—$L_{GF}\, di/dt$
b—ir_{GF} d—field volts (3 generators)

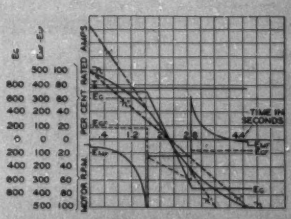

Figure 6. Combination of Figure 2 and Figure 5 to show full operation from +100 to −100 rpm using 75 per cent armature amperes and no overlapping of motor and generator field changes

exciter, Figure 3, the change in motor or generator field will be along an exponential curve composed of four factors.

1. Regulex exciter field time constant (16, 12, Figure 3)

2. Main exciter field time constant (5, 17, Figure 3)

3. Motor or generator field time constant (1, 2, 3, 4, Figure 3)

4. Damping effects of solid magnetic parts, chiefly in the motor or generator frame.

A reversing time of 4.55 seconds as shown to be possible theoretically in Figure 6 has been attained in practice but the accelerating current was not held constant. Furthermore, it has been found in several cases that full advantage could not be taken of this short reversing time because of time required by approach tables, manipulators, screwdowns, and so forth. Further study of these auxiliary devices will, no doubt, be done in the future.

Taking into account these several factors, calculated performance curves of the reversing cycle are shown in Figure 7. These curves include the overlapping of generator field control and motor field control as well as the reducing of the initial decelerating current to 50 per cent, instead of the 75 per cent as in Figure 6, so as to lengthen out the reversing time to approximately seven seconds.

The Reversing Cycle in Practice

The motor shown in Figure 9, drives a reversing mill under the direction of a control similar to Figure 3. The curves of Figure 8 show the various values corresponding to those calculated in Figures 6 and 7, as plotted from an oscillogram of the reversing cycle taken during operation of the mill. It is to be noted that the numerical values of the curves in Figure 8 in general follow those predicted by calculated values represented by the curves of Figure 7. In fact, the values of Figure 8 are somewhat more favorable than those of Figure 7.

The points in the cycle departing from the desired theoretical are:

1. Due to factors 1, 2, 3, and 4, given previously, about one-half second is required for the current to rise, to the desired value at the beginning of deceleration. This lengthens the actual reversing time.

2. There is insufficient motor field voltage during the latter part of the motor field strengthening portion of the cycle, prior to the start of generator voltage reversal. This causes the current to be reduced and with it the rate of deceleration is lessened at this point.

3. Near the end of generator voltage reversal and at the beginning of motor field strengthening there is insufficient forcing voltage on both motor field (E_{MF}) and generator field (E_{GF}) to maintain desired accelerating current. However, as Figure 8 shows, the motor reverses the empty mill in approximately seven seconds with an average armature current of approximately 50 per cent.

Conclusions

1. It is possible to predict by calculation close approximations of current and voltage values during the reversing cycle.

2. By proper selection of exciter voltages and the use of rotating amplifier type control, a shorter reversing time is possible than previously was accepted as satisfactory.

3. By close attention to the factors in machine design affecting time constants, including laminated frames of main motors, generators, and exciters, a still shorter reversing time is possible and problems effecting commutation are made easier.

4. A shorter reversing time requires faster operation of auxiliaries such as approach tables, manipulators, and screwdowns, if an over-all gain is to be realized.

5. It becomes an evident fact from the data that a practical reversing mill control must have exciters with a voltage range 250–300 per cent greater than the required *IR* field voltage. Laminated construction would reduce this forcing voltage requirement approximately 25 per cent.

Appendix

These calculations are for a 7,000-horsepower d-c reversing motor with the following characteristics:

Volts—700.
Amperes—8,000.
Speed—40/100 rpm by shunt field control.
Poles—24.
Armature $WK^2 = 3.22 \times 10^8$ pounds per square feet.
Reversing time = 40/40 rpm in 2 seconds, 100/100 rpm in 7 seconds (accepted commercial practice).
Shunt field = 22 turns per pole.
Shunt field = 0.19 ohms resistance at 25 degrees centigrade.
Shunt field = 444 amperes at 40 rpm, full load; 508 amperes at no load.
Shunt field = 130 amperes at 100 rpm.
Flux per pole = 50.8×10^8 at 40 rpm, no load.
Armature and *IP* resistance = 0.00340 ohms at 25 degrees centigrade.

To change the motor speed from 100 rpm to 40 rpm by field strengthening, the motor field voltage is calculated as that required to maintain approximately 75 per cent of full load armature current during this speed change.

The generator volts remain at 700 throughout. The time in seconds t to change speed from n_o to n_i rpm is

$$t = \frac{WK^2}{(T)} \times (n_o - n_i) \times 0.003252 \qquad (1)$$

which is obtained directly from the fundamental equation:

Force = mass × acceleration

$$T = K_1 \times \phi \times I \qquad (2)$$

Also

$$T = \frac{hp \times 5{,}250}{(n)} \qquad (3)$$

or the full load torque of this motor at 40

Figure 7. The calculated full reversal cycle with the motor and generator field controls partially overlapping for seven seconds total reversing time

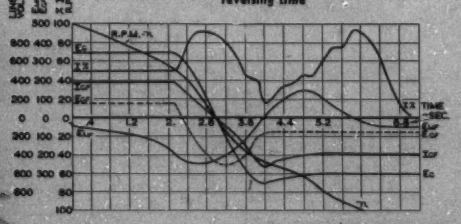

Figure 8. Curves made from an oscillogram of a typical reversal of the motor of Figure 1

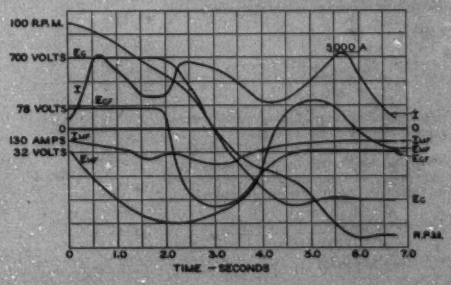

Figure 9 (left). A 7,000 - horsepower, 40/100-rpm 700-volt d-c motor, driving a hot reversing mill

Figure 10. A typical hot reversing mill in action

$$\text{rpm base speed} = \frac{7{,}000\times5{,}250}{40} = 0.92\times10^6$$

foot pounds.

Combining equations 2 and 3,

$$T = 0.92\times10^6 = K_1\times50.8\times10^6\times8{,}000 \qquad (4)$$

or

$K_1 = 2.26\times10^{-6}$ for this motor.

Combining equations 2 and 4

$$T = 2.26\times10^{-6}\times\phi\times I \qquad (5)$$

Combining equations 1 and 5

$$= \frac{WK^2\times(n_o - n_t)\times1{,}440}{\phi\times I} \qquad (6)$$

$$\text{Motor counter emf} = K_2\times\phi_n\times n \qquad (7)$$

or at a base speed, no load,

$$K_2 = \frac{700}{50.8\times10^6\times40} = 0.334\times10^{-6}$$

Then

$$\phi_n = \frac{700}{0.344\times10^{-6}\times n} = \frac{2{,}030\times10^{-6}}{n}$$

Combining equations 6 and 7

$$t = \frac{WK^2\times(n_o - n_t)\times1{,}440\times n}{2{,}030\times10^6\times I} \qquad (8)$$

But n is the average speed between n_o and $n_t = \frac{n_o + n_t}{2}$ so

$$t = \frac{WK^2\times(n_o^2 - n_t^2)\times0.352\times10^{-6}}{I}$$

seconds for the range of motor speed accomplished by motor field change.

Thus using 75 per cent of the 7,000 horsepower full load rating for decelerating from 100 rpm to 40 rpm, and a total motor and mill WK^2 of 3.3×10^6, the time required

$$t = \frac{3.3\times10^6\times(100^2 - 40^2)\times352\times10^{-6}}{0.75\times8{,}000}$$

$$= 1.63 \text{ second}$$

The instantaneous volts required on the motor shunt field to maintain a constant counter electromotive force to produce 75 per cent armature current while motor speed is changing will be

$$e_{MF} = ir_{MF} + L_{MF}\frac{di}{dt} \qquad (9)$$

But

L_{MF} = number of poles × turns per pole × $d\phi$ henries ÷ $di_{MF}\times10^8$ (10)

$= \frac{d\phi}{di}\times5.28\times10^{-6}$, where ϕ = flux lines per pole at i. So

$$e_{MF} = ir_{MF} + \left(\frac{d\phi}{di}\times5.28\times10^6\times\frac{di}{dt}\right) \qquad (11)$$

$$= ir_{MF} + \left(\frac{d\phi}{dt}\times5.28\times10^{-6}\right)$$

Table I and Figure 2 show the required shunt field voltage components ir_{MF} and $L_{MF}di/dt$.

These two components of motor field voltage during field strengthening are obtained as follows:

A. Plot motor speed versus motor field ampere curve, Figure 1.

B. Calculate motor flux versus time (column 2, Table I from equation 7).

Table II. Motor Speed Change During Generator Voltage Reversal Using 75 Per Cent Rated Armature Amperes

(1) n_t	(2) $dt = 0.0156 \times (n_o - n_t)$	(3) t	(4) Gen. Volts	(5) (Figure 5) i_{GF}	(6) (3 Gen.) ir_{GF}	(7) $\times10^6 = \phi$/Pole	(8) $\times10^8 = d\phi$	(9) 18.05×10^8 $d\phi/dt$
+40		0	700	191	78.2	7.66		
	0.078						0.9575	221.5
+35		0.078	613	144	59.0	6.7025		
	0.078						0.9575	221.5
+30		0.156	525	116	47.5	5.7450		
	0.078						0.9575	221.5
+25		0.234	438	94	38.4	4.7875		
	0.078						0.9575	221.5
+20		0.312	350	74	30.2	3.8300		
	0.078						0.9575	221.5
+15		0.390	262	56	22.7	2.8725		
	0.078						0.9575	221.5
+10		0.468	175	37	15.1	1.9150		
	0.078						0.9575	221.5
+ 5		0.546	87.5	18.5	7.6	0.9575		
	0.078						0.9575	221.5
0		0.624	0	0	0	0		
	0.078						0.9575	221.5
− 5		0.702	− 87.5	− 18.5	− 7.6	0.9575		
	0.078						0.9575	221.5
−10		0.780	−175	− 37	−15.1	1.9150		
	0.078						0.9575	221.5
−15		0.858	−262	− 56	−22.7	2.8725		
	0.078						0.9575	221.5
−20		0.936	−350	− 74	−30.2	3.8300		
	0.078						0.9575	221.5
−25		1.014	−438	− 94	−38.4	4.7875		
	0.078						0.9575	221.5
−30		1.092	−525	−116	−47.5	5.7450		
	0.078						0.9575	221.5
−35		1.170	−613	−144	−59.0	6.7025		
	0.078						0.9575	221.5
−40		1.248	−700	−191	−78.2	7.6600		

C. Calculate speed versus time curve a, Figure 2, (columns 3 and 4, Table I), by using equation 6.

D. Calculate $d\phi$ for each increment of speed (column 5, Table I).

E. Calculate $d\phi/di \times 5.28 \times 10^{-4}$ and plot as curve b Figure 2 (column 6, Table I).

F. Record shunt field amperes from Figure 1 for each speed point (column 7, Table I).

G. Multiply field amperes by r_{MF} hot to get e_{MF}, column 8, Table I, and plot as curve c, Figure 2.

H. Add curves b and c Figure 2, to obtain total field volts curve d.

The motor speed change between +40 rpm and −40 rpm occurs with constant motor field of 58 amperes, generator voltage changing from +700 volts to −700 volts at a constant rate which maintains 75 per cent of full load armature current in the loop.

Acceleration time t then proceeds by equation 6.

$$\frac{3.3\times10^6\times(n_o-n_t)\times1{,}440}{0.75\times8{,}000\times50.75\times10^6}$$
$$=0.0156(n_o-n_t)=0.624 \text{ seconds}$$

from 40 rpm to 0 rpm.

The two components of generator field voltage to produce the required voltage change are obtained as follows in Table II.

A. Curve a of Figure 5 shows the relation between time t and speed n for the motor speed change by equation 6 with constant torque value equal to 75 per cent of base speed rating (columns 2 and 3).

B. The 7,000-hp motor is driven by three 2,000 kw generators as shown in Figure 3 whose shunt fields are in series and each have the following characteristics:

Poles—14; flux per pole at 700 volts no load, 93 per cent of synchronous speed = 7.68×10^6; armature and interpole resistance = 0.00975 ohms at 25 degrees centigrade; shunt field = 48 turns per pole; shunt field = 0.109 ohms at 25 degrees centigrade; no-load saturation as per Figure 4, at 93 per cent of synchronous speed; calculate shunt field i_{GF} (column 6, Table II) shown as curve b Figure 5, by the use of saturation curve Figure 4.

C. Calculate flux per pole (column 7, Table II) from the relation: Flux per pole = 0.01096 × volts × 10^6 × 93 per cent of synchronous speed. (see equation 7).

D. Calculate $L_{GF}di$ from the relation

$$L_{GF}=\frac{3\times14\times43\times d\phi}{di\times10^8}$$

or

$$L_{GF}=18.05\times10^{-6}\times\frac{d\phi}{di}$$

or

$$L_{GF}=\frac{di}{d\phi}=18.05\times10^{-6}\times\frac{d\phi}{di}$$

Columns 8 and 9, Table II, shown as curve c Figure 5.

E. Add curves b and c Figure 5 to obtain curve d showing total generator shunt field volts. Figure 6 combines Figures 2 and 5 to show the total speed change of +100 rpm to −100 rpm.

Assuming a ceiling voltage of approximately 250 for both motor and generator field, and a time constant of 1.0 for both exciter systems, a similar calculation to Tables I and II will be made on this more practical basis. The build up of exciter curves are first plotted, then the motor and generator flux obtained by use of equation 11 on a trial-and-error method. Due to the limitation of practical exciter ceiling and response, it is soon found that an overlap of motor and generator field control is required to maintain the armature current, so Figure 7 shows the calculated result using generator field control beginning at approximately 52 rpm on the decelerating cycle. The armature current is figured step by step based on the slope of the speed curve calculated previously from the motor and generator flux curves.

Symbols

Arm = motor armature
IP = interpoles
t = time in seconds
n = speed in rpm
n_o = initial speed rpm
n_t = speed at the end of t seconds
T = torque in foot pounds
K_1 = constant
K_2 = constant
ϕ = flux per pole
ϕ_n = flux per pole at speed n
I = armature current in amperes
WK^2 = inertia of mass in pounds feet squared
L = inductance in henries
hp = horsepower
i_{MF} = motor field current in amperes
e_{MF} = instantaneous motor field volts at field current i_{MF}
r_{MF} = motor shunt field circuit resistance
L_{MF} = motor shunt field inductance at field current i_{MF}
L_{GF} = generator shunt field inductance at field current i_{GF}
i_{GF} = generator shunt field current in amperes
r_{GF} = generator shunt field resistance
e_{GF} = instantaneous generator shunt field volts

Discussion

F. M. Scott (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): I would like to emphasize the statement made by Sellers that full advantage could not be taken of the short possible theoretical reversing time because of the time required by approach tables, manipulators, screw downs, and so forth. In fact, the present standard reversing times cannot fully be utilized because of these considerations. While it is true that the operators can quickly determine the difference when the reversing time is changed, the main advantage of faster reversing cycle is the general feeling of the operator. As Harris mentioned earlier, a peppy mill satisfies the operator but does not necessarily increase the tonnage of the mill. We have found that generally the tonnage remains unchanged by either adding or subtracting a second from the reversing cycle, as the steel is actually in the mill only a short portion of the time over an extended period. So therefore, a further study of the auxiliary devices and scheduling is indicated.

Frequency Modulation Receiver Design Trends

G. G. YOUNG
NONMEMBER AIEE

FREQUENCY modulation had its commercial beginning in October 1940[1] when permits were granted to 15 applicants by the Federal Communications Commission (FCC). These 15 stations were located in areas of dense population resulting in service to 20,000,000 potential listeners located within the 50 microvolt per meter contours (the marginal value required by the FCC). A large number of additional applications were on file with the FCC at that time.

The number of frequency modulation (f-m) receivers in the hands of the public in 1940 numbered only a few thousand. Before the entry of this country into the war, however, approximately 400,000 f-m sets[2] had been produced and sold. Most of these were in the deluxe class. In 1940, f-m transmitters were available at a cost comparable to that of an amplitude modulation (a-m) transmitter of equal rating.

By virtue of its noise reducing properties the coverage of an f-m transmitter is better than that of an a-m transmitter of the same power rating. It is apparent, therefore, that the progress of the f-m broadcasting has depended upon the ability of receiver engineers to provide good quality f-m receivers at a price that the public is willing to pay and also upon the ability of the station operators to furnish programs that are competitive with contemporary a-m programs.

The superior features of f-m broadcasting are well known: freedom from noise; genuine high-fidelity reproduction; and freedom from interference and fading. It has been demonstrated that in these respects f-m is superior to a-m and that these advantages are obtainable in practice. It must be borne in mind, however, that these advantages are obtainable only with a well-designed receiver and that the design requirements for f-m receivers are basically more rigid than for an a-m receiver.

In the long run the service rendered to the public by f-m broadcasting will determine the success of the f-m industry as a whole. Initially, however, f-m receiver sales were stimulated by advertising claims that f-m would open a new and better era in broadcasting. Buyers of f-m sets were motivated by the desire to enjoy the superior performance of f-m and a fear that receivers without the f-m range would shortly become obsolete and would therefore be a poor investment.

It is not the purpose of this paper to prove that f-m is or is not to have a permanent place in domestic broadcasting service. The problems encountered in the design of f-m receivers are more difficult, however, than is the case with straight a-m receivers. F-m receiver production costs remain high even though significant advances have been made in the development of low cost components and design techniques have improved steadily. Some of the design problems and the techniques that have been developed will be discussed. In addition, the factors that have resulted in changing design requirements will be considered and the effect of these factors on the cost and complexity of f-m receivers will be discussed.

Prewar Receivers

An ideal f-m receiver would have the following performance characteristics:

1. The audio system would be capable of good reproduction up to 15 kc.
2. A circuit for amplitude rejection would be included ahead of the detector or the detector would be insensitive to amplitude modulation.
3. The receiver would be sufficiently sensitive to reach the threshold of amplitude limiting when operated in a region where the field intensity is no more than the marginal 50 microvolts per meter.
4. The side responses should be small in amplitude so that the tuning characteristic is agreeable.
5. The transfer networks would be linear in both phase and amplitude in order to avoid phase modulation on impulse noise.

A study of these requirements will reveal that the design of f-m receivers is considerably more exacting than that of a-m receivers.

The only practical f-m receiver circuit in 1940 was the Armstrong system consisting of a superheterodyne receiver incorporating a grid-bias limiter and a phase-shift discriminator. Technically it was possible in 1940 to design an f-m receiver having the performance characteristics outlined before. The design of such a receiver within the reach of the general public was a difficult task, however, and the deluxe console was the most common form of f-m receiver.

The design problems encountered by receiver engineers in the 42-to-50 megacycle (mc) range were not basically different from those encountered in the design of a-m short-wave receivers. Oscillator drift became more troublesome because of the small values of circuit capacity required to tune these frequencies but compensation was not difficult. The problem of microphonics was more pronounced because of the higher operating frequencies and the low circuit capacities and because the f-m detector is frequency sensitive. Stability of the radio frequency (r-f) amplifier stage became a more serious problem than it had been at the lower frequencies, largely because of the impedance of the gang capacitor rotor shaft which introduced coupling be-

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G. G. Young is with the General Electric Company, Syracuse, N. Y.

tween the tuned circuits associated with the plate and grid circuits of the r-f amplifier.

The tubes and components designed for broadcast and short-wave band service perform acceptably at 50 mc. The discriminator circuit, although considerably more complicated and critical than an a-m detector was not new to receiver engineers since use had been made of it in automatic frequency control circuits used in a-m receivers.

Since many of the prewar receivers were designed to reach the threshold of limiting with signal inputs of 10 microvolts or less, the voltage gain required at the intermediate frequency (i-f) was rather high (100,000 times or more). This is more gain than engineers had been accustomed to handling at a single frequency. The intermediate frequency of 4.3 mc chosen to place the image frequency outside the 42-to-50 mc band makes the stability problem much more difficult than at 455 kc. The solution of this problem did not, however, require any particularly new techniques. With experience, engineers were able to obtain stable voltage gains of 75 or more per stage.

Across the 42-to-50 mc band, antenna stage gains of three to four times and r-f stage gains of 18 to 20 times are obtained easily. The use of gang capacitors for tuning, limited the r-f gain of most receivers to something less than 15 times except in the more expensive receivers which used tuning condensers having insulated rotor shafts to isolate the sections from each other in order to prevent coupling between grid and plate tuned circuits. It was this stability problem in the r-f amplifier that led to the development of the General Electric model *JFM-90*, a double conversion superheterodyne.

The *JFM-90* converted in the first stage to a first intermediate frequency which varied from 23.15 to 27.15 megacycles as the receiver was tuned over the 42 to 50 megacycles range. The second convertor mixed the first intermediate frequency with the local oscillator voltage to produce the second intermediate frequency of 4.3 megacycles which was a fixed frequency. A voltage gain of approximately 20 times was obtained in the first convertor. No instability problem was encountered because the plate and grid circuits of the first convertor were tuned to different frequencies. Because of the high order of stable voltage gain obtainable with the double-conversion circuit the *JFM-90* receiver had a quieting sensitivity of approximately 5 microvolts.

Most of the early prewar receivers employed two stages of amplification at intermediate frequency as well as two grid-bias limiters in cascade. Such a circuit, although rather elaborate, was made necessary by the desirability of obtaining acceptable performance even outside of the 50 microvolt per meter service area. The trend in 1941 was definitely toward the dropping of the second i-f stage and it seems certain that the cascade limiter would shortly have been abandoned. The need for more economical designs, the increasing number of f-m broadcasting stations, and the availability of better tubes, such as the *6SH7*, as limiters would certainly justify this trend in design.

To be commercially acceptable the prewar receivers had to include the a-m broadcast band. This fact resulted in receiver designs that were rather complex compared to straight a-m receivers since, unlike the a-m short-wave bands, the f-m portion of the receiver required tuning elements and an i-f channel that were not used for a-m reception. This in addition to the increased design problems due to the higher operating frequencies, the need for an amplitude limiter, and the inherent complexity of a frequency sensitive detector made the prewar f-m receiver necessarily more expensive than the contemporary a-m receivers.

At the late prewar stage of the art, a combination a-m–f-m receiver of good design used only one tube, the limiter, that was not also required in an a-m receiver of good design. If the war had not intervened, and if f-m had been allowed to remain in the 42-to-50 mc range, it is possible that straight f-m receivers might have become inexpensive enough to become popular. This would have depended upon the development of the f-m broadcasting service.

Postwar Receivers

During the war years the FCC became convinced that long distance interference resulting from skywave reflections would become a serious problem as the number of stations increased. Theory and the results of field tests indicated that this type of interference would be negligible in the 100-mc region. During November 1945 the FCC[3] made tests near Washington, D. C., comparing the coverage of a transmitter operating at 99.8 mc to one operating at 43.2 mc. The results indicated that transmission at 99.8 mc was at least as good as at the lower frequency. During January 1947 the FCC[4] assigned frequencies between 88-and-108 mc to all f-m broadcasting stations.

Regardless of the possibility of annoying interference that might have troubled f-m listeners had f-m remained on the 42-50 mc band it must be admitted that the change to the 88-to-108 mc band struck a severe blow to the progress of commercial f-m. What the long run effect of this change will be still remains to be observed. At any rate the change came at a time when f-m seemed ready to make rapid progress. The number of f-m stations in operation at the end of the war was approximately 50 commercial stations plus about 10 experimental stations.[5] All of these were operating in the 42-to-50 mc band and approximately 400,000 f-m receivers were in the hands of the public. Prewar experience, although not resulting in low priced f-m receivers had enabled engineers to solve most of the basic design problems related to the use of the Armstrong circuit receiver. Receivers optimized for operation on the 42-to-50 mc band could have been built to give very good performance on both the a-m and the f-m bands and at a cost very much lower than that of the early postwar receivers, that included not only the 88-to-108 mc band but also the 42-to-50 mc band. It was obvious at the end of the war that a considerable period would elapse before all of the f-m stations were converted for operation at the higher frequencies. Manufacturers felt therefore, that the old f-m band should be included so as to permit immediate service to the purchaser.

Even if the change to the 100 mc band could have been accomplished immediately after the war without the costly and troublesome transition period that was experienced, the problems of receiver engineers would still have been multiplied as a result of this change. Most receiver engineers had little experience with frequencies in the 100-mc range. The experience they did have had been obtained in the design of wartime apparatus where cost was of secondary concern and elaborate precautions could be observed. As a result, engineers had not developed the design techniques that are necessary to obtain good performance at low cost.

Intermediate Frequency Considerations

The desirability of placing the image frequency outside of the 88-to-108 mc band made necessary the choice of an intermediate frequency greater than 10 mc. The 10.7 mc range has become standard with most manufacturers. Whereas only one stage of amplification at 4.3 mc is necessary to obtain the necessary gain and

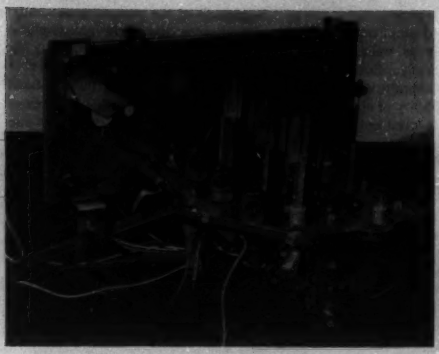

Figure 1(A). 5-band tuner using Guillotines for frequency modulation and short-wave tuning

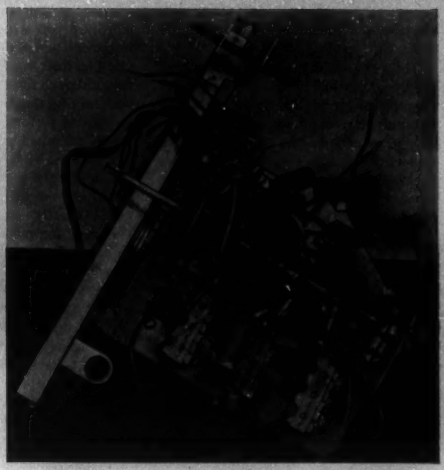

Figure 1(B). Bottom view of tuner shown in Figure 1(A)

selectivity, two stages are necessary at 10.7 mc in receivers that use the grid-bias limiter. The lower gains obtained in the r-f and convertor circuits at 100 mc plus the reduced gain and selectivity obtained in an amplifying stage at 10.7 mc as compared to 4.3 mc make two stages necessary. Stability is, of course, more difficult to maintain at 10.7 mc and the added stage results in increased cost.

Tubes

The most obvious problem confronting engineers at the close of the war was the choice of tubes for the r-f and convertor circuits. The conventional receiver tubes, although good enough for use in the 42-to-50 mc range, were very poor at 100 mc. High input conductance and low effective mutual conductance at 100 mc made most of the conventional tubes poor amplifiers and oscillators. The *6SA7*, at that time the best all purpose pentagrid convertor, is a very poor oscillator at 100 mc. Although the *6SA7* will work with the oscillator at half frequency, the conversion efficiency is too low to be useful.

Good tubes, namely the *6AK5* and the *6AG5*, were available at the end of the war but at a cost several times as high as the conventional tubes. An added disadvantage is the poor performance of these tubes on the a-m broadcast band because their sharp-cutoff grid characteristic results in a very poor automatic volume control characteristic and makes them susceptible to overload, with resultant distortion and cross-modulation effects. Even so, the necessity for obtaining high sensitivity on the new f-m band resulted in the wide use of the more expensive tubes as r-f amplifiers, mixers, and oscillators in the early postwar receivers. The demand for low cost tubes capable of good performance on both the low and high frequencies resulted in the development of the miniature types such as the *6BA6*, *6AU6*, *6BE6*, and others. The *6BE6* is an excellent oscillator at 100 mc and has reasonably good conversion efficiency. The type *6S8-GT* and the new miniature type *6T8* have fulfilled the need for a tube combining the functions of discriminator, a-m detector, and first audio amplifier in one envelope.

Tuning Methods

As a tuning device the ganged capacitor had begun to show some deficiencies in the 42-to-50 mc band. At 100 mc, microphonics, amplifier instability due to grid-plate coupling through the rotor shaft impedance, and high contact resistance at the rotor ground points seemed insurmountable problems to many engineers. As a result, many new types of tuners were developed and used. Perhaps the best of these was the permeability tuned coil[6] used by several manufacturers and the Guillotine tuner[7] developed and used by the General Electric Company. Both of these types of tuners have advantages that make them well suited to operation on the new f-m band. The tuned circuits are small units that may be placed in the optimum position on the chassis so that connecting leads are short. Being inductance tuners they present the best obtainable L/C ratio over the complete tuning range. Since each tuned circuit is an individual unit no common coupling need exist. Both types of tuners are reasonably free of microphonic effects and may be designed to provide consistently good circuit tracking in production. The Guillotine tuner consists of a 2-turn coil made of two flat stamped metal parts mounted in accurately molded plastic frames. The inductance is varied by sliding a metal vane between the two turns. Tracking is controlled by the shape of the vane.

Figure 1(A) shows tuner unit used in the General Electric model *417* receiver. The dial mechanism is in the background. The oscillator Guillotine is in the foreground. The r-f and antenna Guillotines may be seen just back of the oscillator section. At the right of the illustration, the permeability tuned coils for broadcast band tuning may be seen. The Guillotine vanes and the iron cores are supported by the elevator platform which can be seen just above the Guillotines. This platform moves up or down to vary the inductance of the tuning elements.

The disadvantage of both the Guillotine and the permeability-type tuners is that they require rather elaborate mechanical linkages which in conjunction with a gang capacitor or tuned coils for the a-m band results in a very expensive tuner assembly. Thus the need for a single unit tuner which functions for both a-m and f-m is indicated. The return to the use of the ganged capacitor is

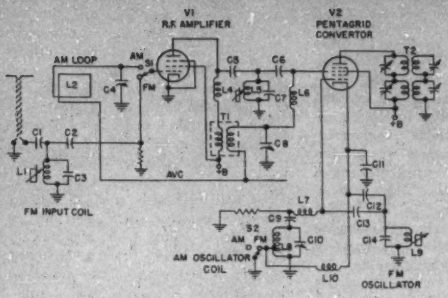

Figure 2. Circuit used for simplified wafer switching

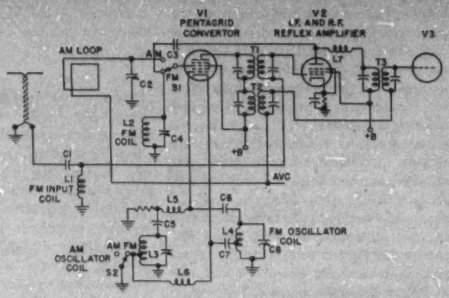

Figure 4. Reflexed radio-frequency amplifier circuit

now a definite trend. Improved design techniques and reduced emphasis on sensitivity plus economic considerations have resulted in more widespread use of the ganged capacitor for f-m tuning. The increased popularity of wide band (fixed tuned) input circuits has hastened the return to the use of capacitor tuning by eliminating the need for a section to tune the input circuit and thereby eliminating the instability problem that resulted from the impedance of the rotor shaft.

Front End Circuitry

The necessity of incorporating both the new and the old f-m bands as well as the a-m broadcast band in the early postwar receivers resulted in very complicated r-f and convertor circuits with involved wafer switching. Many receivers also included one or more short-wave a-m bands which further complicated the design. The need for short connecting leads and simple switching is hard to fulfill when this number of bands must be designed. The use of a pentode mixer with separate local oscillator in many of the early receivers also increased the complexity of the switching. Figure 1(B) shows the wafer switching assembly which is a part of the tuner shown in Figure 1(A). This switch selects either of the f-m bands, a push button or manual broadcast position or one of two short-wave spread bands which were tuned by the Guillotine coils.

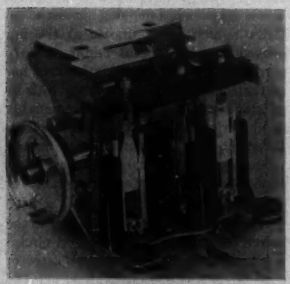

Figure 3. Guillotine tuner used with circuit of Figure 2

The small values of inductance and capacity used to tune the 88-to-108 mc band make wafer switching, particularly in oscillator circuits, very undesirable. A few inches of connecting leads plus the inductance and capacity of the wafer switch contacts may form an appreciable part of the tuned circuit at these frequencies. Temperature and humidity changes cause variations in the capacity of the wafer switch sufficient to cause serious oscillator drift. In many of the postwar sets the wafer switch was the worst cause of frequency drift and certainly the most difficult to compensate because of the wide variations between individual switches of the same type. Another disadvantage of switching in the high-frequency circuits is the difficulty of controlling the length of connecting leads. Variations in the length of these leads cause mistracking of the tuned circuits since the leads are part of the tuned circuits. To compensate for this, the permeability tuners and the Guillotine tuners were designed for a 1-mc overlap at each end of the band. An adjustment of the movable element was provided so that the total range could be adjusted in the direction to compensate for the inductance variation in the connecting leads.

The passing of the 42-to-50 mc f-m band and the elimination of the short-wave a-m bands from most receivers permits considerable simplification of the bandswitching. The use of the *6BE6* type of pentagrid convertor also simplifies the switching and results in a considerable cost saving compared to the earlier convertor circuits which employed two pentodes, each costing more than the *6BE6*, as well as a greater number of components. Figure 2 shows a circuit arrangement which not only reduces switching to minimum, but results in considerably improved performance on the f-m band.

In the circuit of Figure 2, L_1, L_4, and L_5 are Guillotines for tuning the f-m antenna, r-f amplifier, and oscillator circuits respectively. These coils are placed close to the associated tube socket to make the connecting leads short. L_4, the r-f coil, and L_5, the oscillator coil, are wired directly to the proper socket terminals through the coupling capacitors C_8, C_9, C_{12}, and C_{15}. The a-m tuned circuits are connected to the tube sockets through the choke coils L_4, L_6, L_7, and L_{10}, which are made large enough to prevent shunting of the f-m circuits by the a-m coils. S_1 which selects the f-m or a-m antenna circuit and S_2 which disables the a-m oscillator coil when the bandswitch is in the f-m position are the only switches required to change the r-f and convertor circuits from a-m to f-m operation. The a-m oscillator takes control when the receiver is switched to the a-m position. The coupling capacitors for the f-m circuits are small values which act as part of the parallel circuit capacity for the a-m circuits when the receiver is operating on the broadcast a-m band. The chokes used to isolate the f-m circuits act as connecting leads for the a-m coils since their inductance is small compared to that of the a-m coils.

Besides the simplification of the wafer switching, this circuit has the following advantages. The f-m coils constitute nearly all of the circuit inductance because the leads are short and no switching is done except in the grid circuit of the r-f amplifier tube. Temperature and

humidity cause very little change in the capacity across the tuned circuits so that oscillator drift is small and may be easily compensated. The short direct leads in the f-m tuned circuits are easily controlled in production so that consistently good tracking is obtained. Several variations of this circuit have been used in receivers now on the market. One variation, for example, has the high-frequency coils in series with the a-m coils. The f-m coils are wired directly to the tube socket and are not disturbed by switching. The changeover from a-m to f-m is accomplished by disabling the a-m oscillator as in the parallel arrangement of Figure 2.

Figure 3 shows tuner unit used in a General Electric receiver currently in production which uses the circuit of Figure 2. This tuner uses a miniature version of the Guillotine tuner for f-m tuning and a ganged capacitor for tuning the a-m broadcast band. The wafer switch used with this tuner consists of a single wafer which accomplishes the switching functions shown in Figure 2 plus the audio switching required to select the a-m or the f-m detector.

Since two stages of amplification at 10.7 mc are required in receivers using the grid-bias limiter while only one stage is needed for the 455 kc a-m–i-f it is possible to reflex the extra f-m–i-f stage to make it operate as an r-f amplifier on the f-m band. Figure 4 is a simplified circuit showing how this principle is applied in receivers being built by the General Electric Company. This circuit is similar to Figure 2, in that the f-m circuits are connected to the tube sockets through small coupling capacitors and the a-m circuits are connected through chokes to prevent shunting of the f-m circuits. L_1 is tuned to the center of the f-m band by the coupling capacitor C_1 plus the stray capacities and the tube input capacity. C_1 is chosen to make the bandwidth of the input circuit such that the gain at the band ends is about half of the mid-band gain. Broad tuned input circuits have been used in a number of receivers now being marketed. It is possible to obtain performance with this circuit that is essentially as good as with a continuously tuned circuit. The r-f voltage developed across L_1 reaches the grid of the r-f tube through the secondary of the first f-m–i-f transformer. After amplification, the r-f is applied to the convertor stage. The circuits just described make use of the standard low-cost miniature tubes such as the *6BA6* for r-f and i-f amplification and the *6BE6* pentagrid convertor. The use of these tubes plus the simplicity of the circuits results in considerable cost saving compared to the early postwar r-f and convertor circuits.

Detectors and Amplitude Limiters

Most of the f-m receivers built during 1946 used the grid-bias limiter and the phase-shift discriminator. No basic improvements have been made in the grid-bias limiter but its operation was improved over prewar limiters by the use of high mutual conductance tubes such as the *6SH7* and the *6AU6*. It was the practice in prewar receivers to make the discriminator bandwidth approximately 200 kc. The trend since the war has been toward discriminator bandwidths of 400 kc or wider. This results in much lower side responses and a broader main response, so that the tuning characteristic is much improved. The wide main response tolerates a considerable amount of drift before retuning of the receiver is required.

Even with the high mutual conductance tubes now used as limiters, the level required for saturation of the grid-bias limiter makes necessary two stages of amplification at 10.7 mc. This requirement had led to a search for a low-level amplitude limiter. The most successful circuit of this type is the ratio-type detector.[8–9] Many of the f-m receivers and translators manufactured during 1947 and the first half of 1948 have made use of this detector. The advantages are several: (1) no limiter is required ahead of the ratio-type detector, (2) quieting is obtained with about one-tenth of the signal level needed to saturate a grid bias limiter, and (3) the side responses are low and with a well-designed ratio-type detector are quite narrow. Also the side responses are quite close to the main response. The result is a tuning characteristic which appears almost like a single response. The tuning characteristic plus the low noise level between stations give the ratio detector set an agreeable tuning action very similar to an a-m receiver. Although the ratio-type detector has found its principle application in low priced receivers, the linearity and noise reducing properties are sufficiently good to warrant its use in deluxe receivers.

The ratio-type detector has several disadvantages which have caused some manufacturers to continue the use of the grid-bias limiter and phase-shift discriminator. The output of the detector is low, particularly if only one stage of amplification at 10.7 mc precedes the detector driver stage. As a result many signals, sufficiently strong to produce quieting, do not produce enough audio output from the detector to properly drive the audio amplifier of the receiver. Another difficulty resulting from the low audio output is that hum introduced in the diodes of the discriminator proves much more annoying than is the case with the conventional phase-shift discriminator and limiter combination. To obtain good performance from a ratio-type detector, diodes having low internal resistance are required. Until very recently none of the duplex diode triode tubes had sufficiently good diodes for ratio-type detector use. Therefore an extra tube, a double diode, was needed. This offset, partially at least, the saving resulting from the use of the ratio-type detector circuit.

The Trend Towards Reduced Performance

The prewar f-m receivers, in general, were designed to maintain the standards of performance that had been set for f-m. Many of the early postwar receivers were designed with better-than-average audio systems and had excellent noise reducing capabilities. The high cost of a-m–f-m receivers compared to straight a-m sets has forced some sacrifice in performance even though the circuit improvements already described have resulted in considerable cost reduction. Audio range is logically the first to be sacrificed. First of all, most of the program material being broadcast does not make use of an extended audio-frequency response. Secondly, most of the public has not yet learned to appreciate true high fidelity. In fact, a restricted response range is more pleasing to many listeners. The audio systems of most of the a-m–f-m table models and many of the a-m–f-m console receivers built during 1947 and 1948 are no better than would have been used in a straight a-m receiver.

Most of the receivers now on the market exhibit reasonably good sensitivity and noise limiting ability. The necessity of getting away from the use of an outside dipole antenna has dictated against sensitivity reductions as drastic as the improved signal strengths might seem to permit. At least one manufacturer has marketed a table model receiver with no r-f stage on the f-m band. Reasonably good performance is obtained on local signals but the need for preselection and voltage gain at signal frequency make elimination of the r-f stage undesirable for most applications.

A particularly interesting answer to the search for a low-cost f-m receiver circuit is a form of super-regenerative[10] detector used in several receivers now on the market. In one tube, a double triode, this circuit accomplishes all of the func-

tions necessary to recover audio from the received carrier. The absolute sensitivity of this circuit is good, signals of less than 10 microvolts being audible. For reasonably quiet and distortion-free output, however, a signal of 200-to-300 microvolts is required. The circuit has no amplitude rejecting capabilities, being an a-m as well as an f-m detector. The sampling effect resulting from the fact that the detector is insensitive during the major portion of each quench cycle gives the circuit a measure of immunity from impulse noise.

The most serious disadvantage of the super-regenerative detector in its present form is interference with other receivers because of radiation. The most serious radiation is at signal frequency, since nearby receivers tuned to the same signal will be blocked completely or seriously disturbed. Strong radiation occurs at oscillator frequency also, as well as at a large number of other frequencies resulting from the combination of the oscillator, signal, and quench frequencies.

Although most exponents of f-m as a high quality service decry the advent of the inexpensive f-m receiver, a more practical viewpoint should be taken. If f-m broadcasting is to continue to grow, a large number of receivers must be placed in the hands of the public to provide the necessary listening audience. Since February 1948 programs of the large networks have been duplicated on affiliated f-m stations. The user of the low cost table f-m set will use the f-m band as a second broadcast band and, although none of the superior qualities of f-m may be exhibited by some of these receivers, the quality of reception on local signals should be acceptable. With the time on f-m stations made more attractive to advertisers the financial position of the f-m stations should improve enough to enable them to extend their services. In turn, the better service offered by the stations will attract more buyers of f-m sets.

Conclusions

At present only 129 fully licensed f-m stations are in operation.[11] These are the stations that have fulfilled all of the requirements of the FCC. Approximately 400 other stations are operating, most of them on limited schedules. In addition, construction permits have been granted for 783 stations and 86 applications are now pending with the FCC. Many of the stations are located to serve metropolitan areas. Millions of listeners are, therefore, included within the primary service areas of these stations.

The following production figures [2–12] show that the number of f-m set owners has steadily increased since the end of the war. During 1946, 181,045 f-m sets were produced. This number represented about 1.3 per cent of the total receiver production. In 1947 the number of f-m sets produced was 1,089,737 or 6 per cent of the total receiver production. During the first quarter of 1948, the 326,000 f-m sets produced represent 8 per cent of the output of all licensed manufacturers. Approximately 75 per cent of the 1947 f-m receiver output consisted of console-type receivers. During the first quarter of 1948 about 68 per cent of the f-m receivers made were consoles. The remainder of the receivers was made up of table model a-m–f-m combinations and f-m translators for use with standard receivers.

Although the trend is toward f-m receivers with reduced audio quality and reduced sensitivity, with many f-m receivers being produced that do not demonstrate the full advantages of f-m, many manufacturers are still producing receivers of the console type that are capable of demonstrating the superiority of f-m over a-m. These receivers plus the improved service that will accompany the growth of f-m broadcasting, might conceivably cause enough public interest in the noise freedom and high-fidelity of f-m to bring about an increased public demand for better quality receivers. Continued improvements in the art will bring high quality receivers within the reach of more of the public.

The high manufacturing cost of f-m receivers is at present one of the most serious obstacles to the progress of f-m. Even if an a-m–f-m receiver is cheapened and simplified to the point where the f-m band performance is no better than that of the a-m band, the selling price of the a-m–f-m combination table model is two to three times that of an equivalent quality receiver that has only the a-m band. The console-combination receivers do not reflect the cost of f-m as much as do table models since the chassis cost is a smaller proportionate part of the total receiver cost.

An additional factor impeding the progress of f-m that has not been discussed is the matter of television. The public in general has shown more interest in television than has been shown for f-m. At present most of the larger manufacturers are concentrating on television development and production, in an effort to meet the public demand for receivers. Presumably many potential receiver buyers are staying out of the market until television service is available in their area.

The bulk of the f-m receivers that now are in use were bought by the public in response to advertising and because most of the better quality console receivers that have been produced since the end of the war have incorporated the f-m band. The next few years will show whether or not the experience of the public will result in a continued demand for f-m receivers and broadcasting service.

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No Discussion

Progress in Coaxial Telephone and Television Systems

L. G. ABRAHAM
MEMBER AIEE

THIS PAPER describes the coaxial system used in the Bell Telephone System to transmit telephone and television signals.

Development of the coaxial system was started some time ago, with systems working before the war between New York and Philadelphia and between Minneapolis, Minn., and Stevens Point, Wis. Various stages in the progress of this development have been described in previous papers[8,10] and the latest telephone terminal equipment has been recently described.[16] The present paper will outline how the system works and discuss some transmission problems, leaving a complete technical description for a number of later papers.

Figure 1 shows the coaxial routes which are now installed or which will be in service in 1948. There are about 5,000 route miles in service providing almost 3,000,000 miles of 2-way telephone circuits plus television network facilities. The entire program shown on the map for the next few years will roughly triple the route miles and more than triple the circuit miles. Miami, Fla., can now talk over coaxial systems across the continent to Los Angeles, Calif., (if they are on speaking terms), a distance of about 3,000 miles. It is also possible to talk from Albany, N. Y. to Charlotte, N. C. or from Buffalo, N. Y. to St. Louis, Mo., and by the middle of next year, it will be possible to talk from New York to Los Angeles via Chicago over coaxials.

There is a television network working from Boston, Mass. to Richmond, Va., a distance of about 600 miles, with radio relay from Boston to New York and coaxial for the remaining distance. There are dropping points at Philadelphia, Baltimore, and Washington in addition to the terminals. There is a midwestern television network for service this fall to connect St. Louis, Chicago, Toledo, Cleveland, and Buffalo by coaxials with Milwaukee, Detroit, and other points along the route connected in by radio relay. Early in 1949, it is expected that these two networks will be connected together by coaxial cable from Philadelphia to Cleveland via Pittsburgh, with a maximum television circuit distance of about 1,500 miles.

The *L1* coaxial system is capable of transmitting, on two coaxials and their associated equipments, 600 telephone circuits or alternatively a 2-way television circuit 2.8 megacycles wide. This system has presented a number of difficult technical problems. The step from the older cable carrier systems having 12 channels to a system with 600 channels was a large one and television transmission introduced additional requirements over telephone transmission. The total cable loss for a 4,000-mile circuit is about 27,000 decibels at the top of the transmitted band and about 4,000 decibels at the bottom, thus requiring large amounts of gain and gain equalization. But large numbers of systems are now working and the solutions to remaining problems seem foreseeable or in hand.

General Arrangements

Types of Cable. In the coaxials made before the war, the inside diameter of the outer conductor was 0.27 inch. The diameter of the inner conductor was 0.072 inch. Hard rubber disks spaced about 3/4 inch apart separated the inner and outer conductors. The outer conductor was 10-mils thick with a longitudinal seam. Over the outer conductor were two steel tapes applied helically along the coaxial, the second tape overlapping the gap between successive turns of the first. Experimental sections were installed before the war with polyethylene disks used rather than the hard rubber disks, because of their lower losses at high frequencies and since the war polyethylene disks only have been used.

The cable now being made is all about 40 per cent larger with an inside diameter of the outer conductor of 0.37 inch,[13] with an inner conductor diameter of 0.10 inch and with a thickness of the outer conductor of 12 mils. The polyethylene disks are spaced about 1 inch apart, but in general design it is identical with the smaller coaxial. A typical arrangement of the eight coaxials of this type in a lead-covered cable, with the associated service pairs and other conductors is shown in reference 15.

These cables are generally buried directly in the ground or in ducts, both for protection and to reduce the amount of temperature variation which would tend to make quite difficult equalization of these cable systems to the precisions desired. Typical attenuation and impedance of the 0.37-inch coaxial cables are also shown in reference 15.

Since only the 0.37-inch coaxials are being installed now, the remainder of the paper will consider only this size, with repeater spacings, and so forth, being given only for the larger coaxials. In general, the 0.27-inch coaxial spacings are less than the 0.37-inch coaxial spacings, with certain spacings limited by power feed to half those of the larger coaxials.

Layout. The line of the coaxial cable system consists of the cable plus main repeater stations at fairly wide intervals with auxiliary repeater stations between them at much closer intervals. The main repeaters are separated by varying distances, depending upon the points at

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L. G. ABRAHAM is with the Bell Telephone Laboratories, Inc., New York, N. Y.

which it is desired to drop circuits and upon other factors. The maximum distance between main repeater stations is about 165 miles, being limited by the maximum voltage at which it is practicable to feed power from the main repeaters to the auxiliary repeater stations. The average distance is 105 miles, with a minimum distance of about 40 miles.

The auxiliary repeaters are spaced at approximately 7.9 miles, the precise spacings depending upon the average temperature of the region and, of course, upon practical problems of putting the repeater points at accessible locations. The auxiliary repeater stations are usually concrete block huts[13] without heat, power, or light, except the repeater power supplied over the centers of the coaxials. In rare cases, they may be in existing buildings or in manholes where a separate building is not practicable, as in large cities. The main repeater stations are typical Bell System buildings with electricity, reserve battery supply, testing means and heat, although these will often be unattended also.

The main repeater stations may be either switching or nonswitching stations as will be explained later. The average switching section is about 110 miles.

Dropping and branching points[16] are places where telephone or television circuits are dropped, where some of the telephone circuits take different routes from others or where a television circuit goes in more than one direction. These are always at switching points to insure continuity of service when switches occur. The general layout of such a station when it is a television dropping point will be described later.

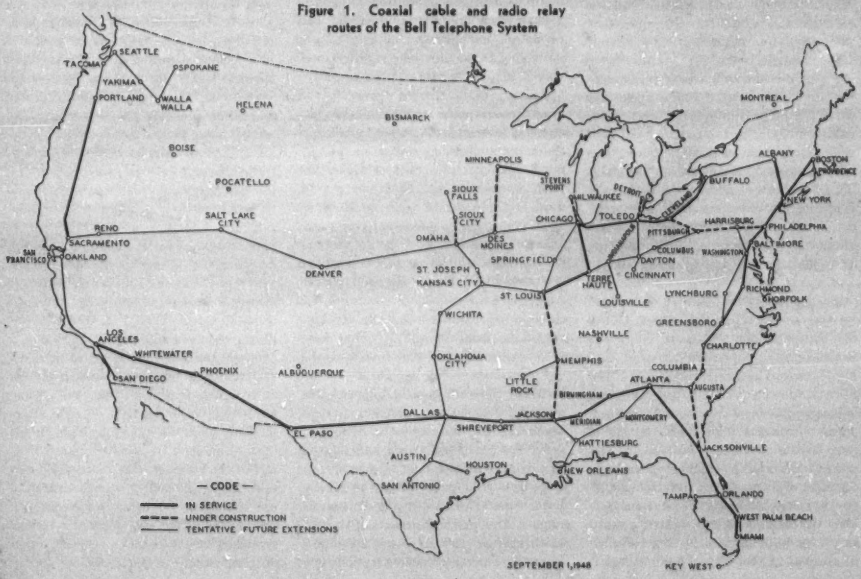
About every 800 miles along present coaxial telephone systems, all the coaxial lines are brought down to supergroup frequencies, group frequencies, or voice frequencies. It is thus possible to adjust the equalization of the high-frequency line and to rearrange the supergroups or groups as required to avoid excessive modulation. These points also provide patching means for flexibility purposes where any group of 60 circuits may be patched to any direction and in any combination of supergroups.

Outline of Functions. Figure 2 shows a simplified schematic of the main parts of a high-frequency line. At the left in a terminal station is a telephone or television terminal. Next in the upper part is a box marked *DE*, which would be delay equalizers on a television line or might be merely a pad on a telephone line. Next is a hybrid coil which divides transmission between the working line and the spare line. The transmitting amplifier *T* raises the level of the signals for transmission into the coaxial cable.

Finally, power separation filters *P* are used which are designed to enable the 60-cycle a-c power for the auxiliary stations to be applied to the centers of adjacent coaxials without interfering with the high-frequency circuit. This power feed is a series power feed, that is, there is substantially constant current throughout the line at all points, with the voltage to ground therefore decreasing as the distance from the power feed point increases. Of course, in a system with capacitance to ground, the power line current would not be quite constant because of the shunt currents, and systems would be limited when the current had dropped too much to permit adjusting the auxiliary repeater to obtain the proper voltages. A power loading system is used which effectively loads the power circuit after it has been taken off the line at auxiliary repeater points and this permits power to be fed over the distance mentioned before.

The next box on Figure 2 shows the layout of an auxiliary repeater. The power separation filters take the power current off the line and through a series transformer winding. There are adjustments for minor residual variations in the line current and to allow for aging in the selenium rectifiers used to produce *B* supply for the amplifiers and regulators. The basic equalizer *E* and the amplifier *A*

Figure 1. Coaxial cable and radio relay routes of the Bell Telephone System



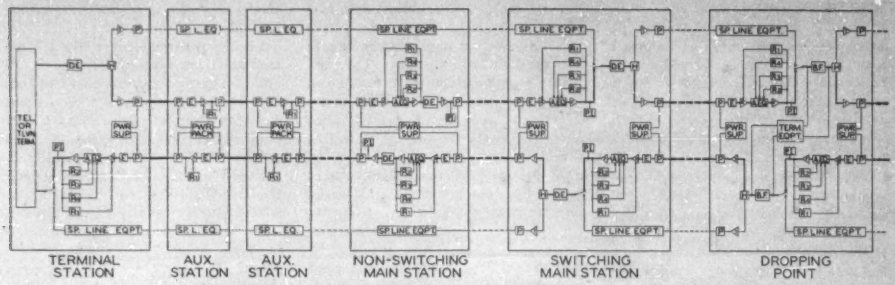


Figure 2. Schematic layout of a coaxial system

together correct for the line loss in the preceding cable section over the band from 60 kc to 3.1 megacycles. At the amplifier output is bridged a pilot regulator R_1 which measures a 2,064-kc pilot sent over the line and adjusts the amplifier gain to keep the pilot level constant by changing the resistance of a thermistor in the amplifier beta circuit. At approximately every other auxiliary repeater point, a manual regulator is used which does not measure the 2,064-kc pilot, but holds the amplifier gain at a value the maintenance man sets by manual adjustment. Changes in loss of the manually regulated section are taken up by the pilot regulator at the next station. The manual regulators are cheaper than pilot regulators, require very little maintenance as units, and reduce the total number of pilot regulators in tandem with advantages discussed later.

Figure 3 shows an auxiliary repeater in its apparatus case. These apparatus cases are hermetically sealed and desiccant is included in the box to avoid possible effects of moisture condensing in the case when the temperature decreases. This sealed case is essential where the case is in a manhole which might be filled with water and in all cases avoids possible effects of excessive moisture on gain characteristics and damage from rodents and insects.

The regulators, amplifiers, power transformer, power separation filters (PSF) and power loading units are indicated. Between the amplifiers are jacks and testing means. The power separation filters have monitoring jacks for test purposes. When a pilot regulator is used, rectified current from the pilot flows through a relay in the pilot alarm unit (below the power transformer) holding it at the center point of its range. If the pilot should go down considerably or become much too high, the pilot alarm unit will put a short across the pilot alarm pair, thus bringing in a signal at the nearest main repeater station which can then be investigated by the attendant at that station. Below the main panel at some points are other panels which vary from point to point. On these are placed power loading coils, building out line sections, and so forth, as required. In the center at the bottom are the four connections between the panel and the lead covered cable.

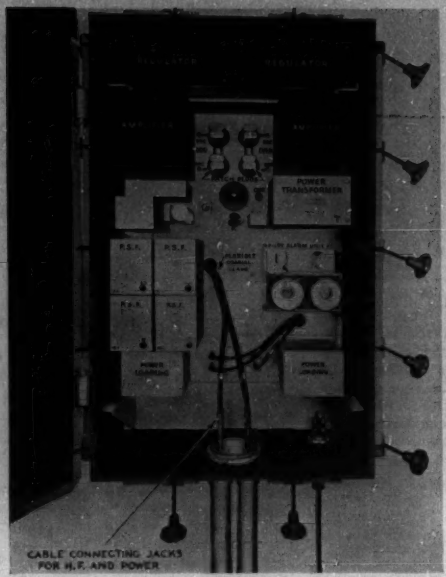
In addition to the auxiliary repeater boxes, each auxiliary station has a box containing a small cross connection frame for the pilot alarm pairs and the order wire, a telephone set, and so forth. The order wire is used by the maintenance man to call in to an attended repeater station and get help of various kinds as required. The regulators, amplifiers, and pilot alarm units plug in, so they may readily be removed for maintenance purposes.

As may be seen from Figure 2, the nonswitching main repeater station has input and output circuits very similar to those at the auxiliary station or the terminal station. The power separation filters connecting to the power supply come first. Next, is a basic equalizer (E) as at the auxiliary point and then a receiving amplifier (R). This amplifier is very similar to the auxiliary amplifier except that its output is adjusted to have a good 75-ohm impedance instead of the variable impedance used at the auxiliary station to improve the signal-to-noise ratio. After the receiving amplifier is the "A" equalizer. This equalizer is controlled by three additional pilots sent over the line at 64, 556, and 3,096 kc. In each case, a regulator measures the amount of power at these frequencies and adjusts certain loss shapes in the "A" equalizer to keep the over-all gain constant at nearby frequencies. There are also a considerable number of manual adjustments of the loss which may be used to correct for differences between main repeater sections due to length and due to the particular amplifiers and repeater panels in that section.

At the output of the "A" equalizer at switching main stations is a flat gain amplifier to bring the level of the telephone or television channels back up to normal. In most cases, the flat gain amplifier and delay equalizer will be omitted at nonswitching main stations. At the switching main station, there may also be a "B" equalizer and another flat gain amplifier. These are placed at intervals to mop up residual gain variations. At the output of the flat gain amplifier are the pilot indicators. At each of the four pilot frequencies, a filter picks off the particular pilot and an amplifier rectifier shows the level at that point on a meter so that in case of trouble it is possible at a glance to see whether the trouble is located in a particular frequency region.

Switching main repeater stations as in the next box to the right on Figure 2 are very similar to nonswitching main repeater stations except for the fact that they are switching points. The present switching system provides for automatic switching of the spare line to replace one working line and manual patches may be made to replace other working lines.

Figure 4 pictures a typical layout of apparatus at a switching main station. A dropping or branching point such as shown at the extreme right on Figure 2 is always a switching point. A given television picture or a given group of 600 telephone channels will always appear in a common part of the circuit at a given switching main repeater station, although at any given time it may be passing over any one of several coaxial lines at intermediate points. In the telephone circuit case, there may be band elimination filters in the common part to suppress certain frequency ranges, thus permitting use of a given frequency range on both sides of the office. There are also similar arrangements to permit dropping sound program circuits as required.



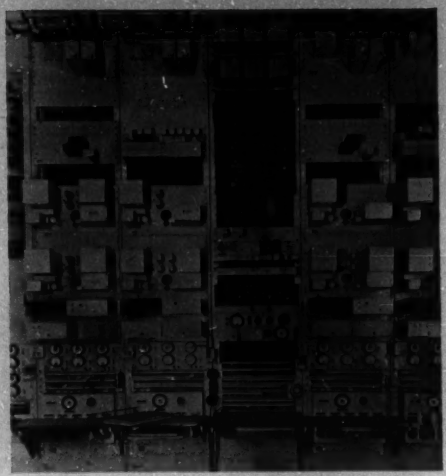


Figure 3 (left). Auxiliary coaxial repeater

Figure 4 (right). Main coaxial repeater station

Figure 5 shows typical dropping arrangements on a television circuit. The television signals come in from the coaxial cable through the receiving amplifier, equalizers, flat gain amplifier *F* and the switching circuit. Next is a hybrid coil *H* which permits through transmission to go on while a local connection is bridged. The through circuit passes through coaxial television switching equipment *TV. SW.* to equipment *D. E.* which provides delay equalization for the succeeding line. The coaxial television switching equipment may be used to change this point from a bridged dropping point to an originating picture point.

The bridged receiving equipment is self-explanatory. Adjustable equalizers for the local picture which do not affect through transmission may be needed as shown. On the transmitting side, pilot supply to provide the four line pilots is necessary when those from the line on the left are interrupted by the *TV. SW.*

The method of operation of the television terminal was described in reference 10 and is still essentially the same except that there is now a 556-kc line pilot which is eliminated at the receiving end in the same way as are the 2,064 and 3,096-kc pilots. This terminal uses vestigial sideband transmission with a carrier at 311 kc, the vestigial sideband extending down to about 200 kc. The sound program terminal[18] is essentially the same as that used for program transmission over other wide band systems. It transmits the sound by single sideband in the range between 80 and 88 kc.

Maintenance Method. The maintenance of coaxial systems is somewhat different from that of previous cable systems. The general philosophy is to merely check circuit performance at the many amplifier points and to return suspected apparatus to more central points for complete testing. The auxiliary stations do not contain testing equipment of any kind. They are maintained by a man who generally covers somewhat less than 200 miles of line by visiting the auxiliary stations on a routine basis or in case of trouble. The repeaters are tested and doubtful equipment removed and replaced with new equipment. The removed equipment is then returned for further tests at a main station called a regulator maintenance point. These points are roughly 150 miles apart along coaxial systems. The three bays at the left of Figure 6 comprise the testing equipment. This is used to test and repair regulators, pilot alarm units, repeater panels, and miscellaneous small items. In addition, amplifiers can be tested approximately by this equipment but are not repaired from these tests. In case an amplifier seems to need to be repaired, it is shipped to an amplifier maintenance point, often many miles away, as there are only five such maintenance point scattered over the country at present.

In the case of Figure 6, the amplifier maintenance point is in the same room and comprises all the other equipment shown. At these points, amplifiers are first opened since they are hermetically sealed, the older ones being soldered, but the newer ones using a pressure seal. The gain characteristics are measured with great precision in a recording gain set. There are various other items of testing equipment permitting precise checking of vacuum tubes, measuring modulation, adjusting impedances, and making various adjustments. After repair, the amplifiers are again sealed and returned for service use.

Portable measuring equipment is taken by the maintenance man to the auxiliary points in a truck. The power meter when plugged into the auxiliary panel permits measurement of all the essential power voltages. The portable pilot indicator may be plugged in to measure the power of the 2,064-kc pilot at the output of the amplifier and to allow small adjustments as required in the regulator sensitivity or in the manual regulator setting. The transmission measuring set is a tuned detector which may be used to measure at

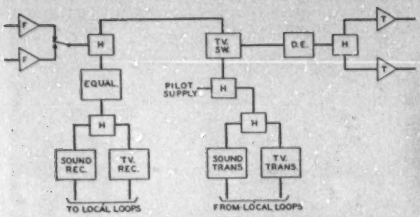


Figure 5. Schematic layout of television dropping arrangements

Figure 6 (right). Regulator maintenance equipment and amplifier maintenance equipment

the input or output of the amplifier. Other equipment available in the truck includes an oscillator which supplies any one of five frequencies and is used in combination with the transmission measuring set for cable measurements when trouble is suspected there. There is also equipment to measure the modulation products of the amplifier in place and miscellaneous items used at rare intervals.

When trouble is indicated, the man at the auxiliary station may call an attended point and have the spare line in that direction switched to include the particular repeater, thus allowing the amplifier or regulator to be changed. If the panel has to be changed, not only must both of the lines be made spare but also the power on the center conductors must be turned down to avoid possible danger and damage.

At the main station, the testing equipment items are larger although they are commonly mounted on racks equipped with wheels so that they may be moved from place to place. The main items are:

1. An oscillator supplying any frequency from about 50 kc to 3.5 megacycles.

2. An analyzer, old or new models of a tuned detector with which it is possible to measure at any frequency from 50 kc to 3.5 megacycles at powers down to about −110 decibels below one milliwatt. It is also used to make line modulation measurements.

3. The transmission measuring set, really a sensitive thermocouple which permits measurements at any frequency over the band without tuning. In order to avoid interference from the line pilots when this measurement is made, it is necessary to have the power at the testing frequency considerably larger than the power of the pilots.

4. The thermocouple meter, used to calibrate the power sensitivity of various items of testing equipment. The calibration essentially depends upon the accuracy of a meter and an impedance. Direct current from a battery is used to adjust its sensitivity, and the sensitivity of the other equipment such as the analyzer is compared to that calibration.

At the main station, it is also possible to determine the auxiliary repeater station from which alarms are received. This is essentially a d-c bridge measurement of the resistance of the pilot alarm pair to the point at which a short is applied when an alarm is given. It is also possible to release temporarily the alarm relay at the auxiliary point and if the trouble has cleared, it is thus set for future action.

It has been found practicable to station men permanently only at occasional main repeater stations, if they are not needed for other local work. In order to be sure that trouble will not ensue from this, a special control system has been devised. This is a system which, at an unmanned main repeater station, continuously inspects a large number of circuit functions and returns an alarm to the nearest attended station if anything performs unsatisfactorily. When an alarm has been returned, the attendant at the distant station may request information from the unattended station which is then furnished in some very considerable detail, there being about 150 separate indications of what is going on at the unattended station. It is also possible for the attendant at the distant station to direct certain actions to be taken at the unattended station, such as switching a particular working line to the spare line, and so forth. By the use of this system, there are cases in which 24-hour main repeater station attendance is required only at wide intervals up to 650 miles.

At television terminals, there is special testing equipment designed to be used to test the television equipment. Primarily, this consists of a transmission measuring set and oscillator which are able to test up to the 8-megacycle carrier used at the television terminal. It also comprises monitors and "A" oscilloscopes for testing television pictures directly, and associated apparatus such as a 311-kc oscillator to supply this frequency as a carrier when a television signal is not available, a looping filter for connecting a transmitting terminal directly to a receiving terminal, and so forth.

Security. Very great care is taken in the design of this system to avoid failure of service. As discussed before, the cable is usually placed underground where it is less susceptible to damage and every care is taken to avoid losing a cable due to natural events. For example, in crossing a known earthquake fault, the cable is coiled back and forth in a bed of gravel so that considerable movement may take place without breaking the cable. As another example, in crossing large rivers, there are commonly two submarine cables placed over somewhat different crossings and half the coaxials are routed through one cable and half through the other, with sufficient capacity in either cable to carry the entire group of coaxials if necessary.

The power supply is designed to be reliable. The load of supplying 60-cycle power will automatically fall back on storage batteries at the main repeater station if commercial power fails. Some systems use rectifier-inverters which electronically generate 60 cycles from the direct current of the storage batteries. In the newer systems, motor-generator sets are used which run from commercial power normally, but with power to the coaxial line supplied from a generator mechanically coupled to the a-c motor rather than directly from the commercial power supply. In case the commercial power fails, a d-c motor which is rotating on the same shaft is automatically connected to the storage battery and drives the a-c generator without substantial loss in time or voltage. This scheme avoids various effects of commercial power variation and superimposed noise in some sections.

The amplifiers are all equipped with parallel tubes and one tube in any or all stages could fail without causing any substantial detriment to the circuit as a whole. The spare line, of course, also allows for a complete amplifier or coaxial failure, being

switched in automatically when the working line fails, without substantial interruption to any service. There are, of course, gas pressure alarms on the cables which come in if there is any substantial leak in the sheath, alarms as described before at each auxiliary station if the pilot levels should go out of limits either on the high side or the low side and alarms of many kinds at unattended main stations to indicate potential trouble at these points. Of course, the security of any such system finally is determined by the goodness of the design of the equipment, by the quality of manufacture, and the excellence of the maintenance which finds potential trouble before it can affect service.

Transmission Problems

Coaxial systems are designed to give satisfactory telephone or television circuits for up to 4,000 miles. This comprises about 500 repeaters in tandem or approximately 250 regulators. In order to make this system satisfactory for such lengths, very considerable design advances have been necessary.

Regulation. Figure 7 shows schematically the layout of a regulator to measure the 2,064-kc pilot and adjust the gain of the amplifier to hold the output level constant. 2,064 kc is picked off by the crystal pilot pickoff filter *P. P. F.* bridged across the line and is amplified and rectified as shown. The amount of direct current at the point marked determines the output of a 2-kc oscillator and part goes to a pilot alarm unit *P. A.* which gives an alarm if the pilot level deviates too much. The oscillator output is fed into a thermistor *T* in the beta circuit of the amplifier. The amount of 2,000-cycle current determines the amount of heat in the heater of this thermistor, controlling the temperature and therefore the resistance of the thermistor, and the resistance of the thermistor determines the gain of the amplifier. Changes in amplifier gain follow a square root of frequency characteristic over the band from about 50 kc to 3.1 megacycles.

The combination of the amplifier and regulator comprises a feed-back circuit at very low frequencies and if the phase of the feed-back circuit became too great at any frequency at which there was gain around the path, singing or "hunting" might occur. The principles of feed-back design were applied to the circuit, and equipment for measuring such a circuit was devised. The practical difficulties may be appreciated when it is pointed out that the frequencies of interest are from 0.01 cycle or less to at least 100 cycles.

With good individual stabilities, the matter of tandem instability had to be considered. When there is a sudden small change in pilot level near the sending end of a long circuit equipped with dynamic regulators, a much greater change may be obtained temporarily at the receiving end of the circuit, such as is shown in Figure 8. Such regulators are said to have tandem instability. In general, long circuits equipped with such regulators will also have "jitter," which is indicated by continuous random and erratic variations in the received pilot. This is due to an accumulation of many very small fluctuations in gain, presumably because of 60-cycle power changes.

The amount of tandem instability depends upon the gain around the local feed-back path in each regulator, as well as its phase, and therefore varies with the length of the repeater section and the temperature of the cable as well as the adjustment of the regulator and other factors. The regulators now being produced are satisfactory for all present telephone and television circuits and for those contemplated in the near future. Very long television circuits will require further advances from this standpoint, such as pilot suppression and resupply at wide intervals or other means.

Gain Equalization. On a 4,000-mile circuit there will be, say 500 repeaters in tandem, making up for a loss of about 27,000 decibels at 3 megacycles and about 4,000 decibels at 60 kc. On a television circuit we might take ±1 decibel as the allowable variation from flat gain in the over-all circuit over the range from 200 kc to 3.1 megacycles. Of course, the true objective is much more complicated than this, varying with the frequency and so forth, but this will serve to illustrate the problem. To obtain this accuracy by making each repeater sufficiently precise, the average gain per auxiliary repeater section would have to be kept flat within ±0.002 decibel. For telephone circuits, considerably wider limits on the high-frequency line gain are possible because of the intermediate and terminal points where the gain of groups, supergroups or individual channels can be adjusted separately based on measurements of the over-all system.

It is not practicable to manufacture repeaters to any such accuracy as a few thousandths of a decibel and too many elements would be required for a design with this accuracy. The gain cannot even be measured to such accuracies. The sources of deviations from the desired characteristics and the shapes of such deviations versus frequency can be studied. Element values can be controlled and measurements can be made as accurately as possible, but when all this is done, one repeater section can still be checked only very unprecisely. What can be done is to put a number of repeater sections in tandem and measure them. This reduces the measured error per repeater section by a divisor equal to the number of such sections measured. By this means, the average characteristics of the repeaters and of the basic equalizers which go to make up the over-all characteristics of the auxiliary repeater sections and of the main repeater sections can be determined. From this information, "A" equalizers for the main stations can be designed. A considerable number of such main sections equipped with "A" equalizers can be measured and "B" equalizers specified to mop up the deviations of a few hundred miles.

In manufacture by the Western Electric Company, the amplifiers are held to roughly ±0.25 decibels over the frequency range of interest. At one time, comparison with reference amplifiers was used to determine that the desired gain characteristics were obtained. But these reference amplifiers aged and changed in gain characteristic so that it was a never-ending struggle to keep them alike and with the proper characteristics to the necessary precision.

The so-called absolute gain standard has been adopted where each amplifier is measured on an absolute basis in a panel which is controlled to have very nearly the impedance and losses of an average field repeater panel plus the preceding line section. The control of impedance is essential because the auxiliary repeater panel impedance does not match the line impedance, particularly at high frequencies. This was intentional in the design and very substantial advantages in signal-to-noise ratio for a given repeater spacing were obtained in this way. But, it did make the gain sensitive to the impedances in which the amplifier was measured.

Other changes have been made in the amplifiers to reduce variations in their gain characteristics with time and between amplifiers. These variations were very small individually, being a few hundredths or at the worst tenths of a decibel per repeater section over a 3-megacycle band. Nevertheless, they were of considerable importance in planning 500 amplifiers in tandem with the over-all precisions necessary for television transmission. The sources of such variations have been located and changes made to reduce them to negligible proportions.

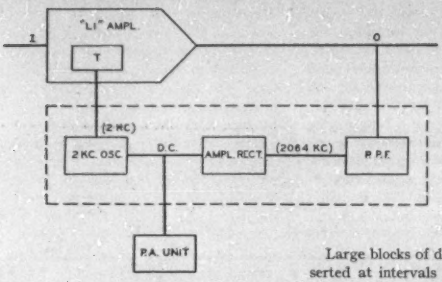


Figure 7. Schematic layout of a regulator and amplifier

They consisted mainly of stray capacitance couplings which affected feed-back paths or phase margins by small amounts.

Phase and Delay Equalization. Phase and delay characteristics of wide band systems are not particularly important on telephone circuits because only about 3,500 cycles in one piece is of any importance. On television circuits, however phase equalization is just as important as gain equalization. In fact, the same magnitude of picture ghosts can be produced by gain or phase deviations, equating 1 decibel to about 7 degrees.

Of course, difficulties with amplifiers as to gain equalization also cause difficulties with phase and delay equalization. Also, such variations in gain tend to confuse the issue as to data on delay characteristics, but as the amplifiers are stabilized, the delay characteristics become more manageable.

The basic phase distortion may be correlated with the band-pass gain characteristics of the television circuit. This system appears to be substantially a minimum phase system, that is, the phase distortion characteristic could be predicted rather precisely if the gain characteristic from zero frequency to infinite frequency were known. Unfortunately, the gain characteristic cannot be measured far enough down into the high loss region at either end of the transmitted band, so the phase or delay distortion must be measured separately.

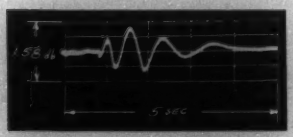


Figure 8. Variation of 2,064-kc pilot and end of circuit due to 0.1 decibel step at input

Large blocks of delay equalizers are inserted at intervals along the line to approximately compensate for the delay distortion of the succeeding line section. To keep the number of types of such blocks at a minimum, delay building out networks are used to build out the delay of the particular delay section by amounts up to two auxiliary repeater sections, and this also provides some adjustment for differences between sections and for changes of the line with time and temperature.

Noise and Modulation. Any telephone or television system must have a satisfactory signal-to-noise ratio and must be free of excessive modulation. From a noise standpoint, the present coaxial system seems generally satisfactory. Certain sources of 60-cycle power noise have had to be cleaned up, but otherwise the measured results just about fit the design values as expected. Of course, such noise can be controlled by varying the repeater spacing. Figure 9 shows typical noise on a 200-mile circuit. It may be seen that the noise is fairly constant with frequency up to about 2 megacycles and then increases. Television circuits can tolerate much more noise at such high frequencies and the few telephone circuits above 2 megacycles are used only for short distances, so the higher noise there is not objectionable.

Modulation is a tougher nut to crack. There are innumerable modulation products to consider. For example, each telephone channel A modulates with every other telephone channel B, producing $A \pm B$, $2A \pm B$, and so forth, products, many of which fall into some other channel and may cause objectionable noise or crosstalk. Each of the pilots which control the gain characteristics will modulate in various ways with each of the telephone channels, with other pilots and with carrier leaks. In the case of television circuits, the pilots all modulate with the television signal, particularly with the carrier at 311 kc. The 311-kc carrier produces in the line second, third, fourth, and up, order harmonics and other products.

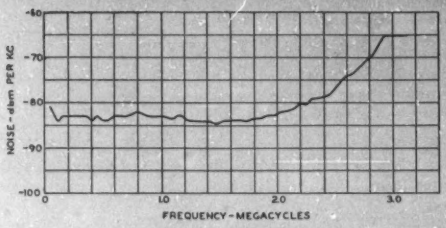


Figure 9. Noise at the −10 decibel level point on a 200-mile coaxial circuit

The modulation products add in various ways as the length of line increases, accumulating different modulation products from each amplifier. Typically, the $A \pm B$ products, for example, which are produced in different amplifiers, will be somewhat out of phase relative to the products in previous amplifiers so the successive products will add in a somewhat random manner or roughly as the sum of their power ratios. Some of the products, notably the $2A$-B products, may have very little, if any relative delay difference between the two modulating frequencies and the product created at the previous amplifier, so successive sections may tend to add directly or as the sum of the currents. On television lines which are phase equalized, this tendency will be even greater.

Most modulation products could be improved by reducing the level of the signals at the expense, of course, of a poor signal-to-noise ratio due to tube noise and resistance noise. For example, in a simple cube law amplifier, if the television signal were reduced to 1 decibel, its second harmonic will be reduced 2 decibels which is 1 decibel relative to the signal. The third harmonic will decrease 3 decibels or 2 decibels relative to the signal, and so forth. For modulation products of, say, telephone signals against any fixed tone such as the pilots, however, such an improvement cannot be obtained. For example, the second order products decrease 1 decibel when the signal is reduced 1 decibel, but this gives no net advantage in the ratio of modulation products to the signal. A third order product of a frequency twice the pilot minus the signal will also not be improved when the signal is reduced.

It seems pretty clear that telephone service over our longest coaxial systems will be satisfactory from a modulation and noise standpoint, since 3,000-mile systems equipped with a considerable number of circuits are already giving satisfactory service. On television circuits, there is

not enough data as yet to be sure. Certain improvements which can be made in the television terminals to give improvements in modulation products for a given signal-to-noise ratio make it seem likely that commercial television pictures can eventually be transmitted across the entire country by coaxial systems.

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No Discussion

Bridge Erosion in Electrical Contacts and Its Prevention

W. G. PFANN
NONMEMBER AIEE

ALTHOUGH the more severe forms of erosion in electrical contacts are due to electrical discharges, some damage is caused in d-c circuits by the molten bridge which exists momentarily between two contacts as they separate. Bridge erosion predominates in contacts of low-voltage circuits having little inductance and it generally produces a spike on the negative contact and a closely fitting crater in the positive contact. Although the transfer may be quantitatively small, locking or premature closure may result, especially if the contact force is low or the contact gap is short. Noble metals and their alloys are particularly prone to this form of erosion.

Attention was called to the molten bridge as a source of material transfer in electrical contacts by Betteridge and Laird.[1] The physics of the molten bridge and data on the transfer attributed to the molten bridge are discussed by Fairweather,[2] by Holm,[3] and in a recent comprehensive study by Lander and Germer.[4]

This paper describes a correlation between the shape of the molten bridge and the transfer of material accompanying its formation and rupture. It is shown that such transfer can be prevented in electrical contacts operated under conditions in which bridge erosion is stronger than, or comparable to other transfer processes. The method involves the use of dissimilar materials in the contact pair and has so far been tried principally with noble metals, but with certain restrictions it should be applicable to base metals. The experimental work which led to this finding will be described briefly and the conditions for preventing bridge erosion will be given in some detail.

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W. G. PFANN is with the Bell Telephone Laboratories, Inc., Murray Hill, N. J.

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Testing Conditions

The testing circuit was essentially a series arrangement of a source of voltage, a resistive load, and the contacts. Inductance and capacitance were reduced to small values in order to minimize the dissipation of energy at the contacts on break and make respectively. The inductance of the battery leads was effectively eliminated by placing capacitors across the leads at a position physically close to the contacts, as shown in Figure 1. The inductance of the remainder of the circuit, loop *abcd*, was 3×10^{-7} henry and the distributed capacity of *abcd* measured at the contacts was 8×10^{-13} farad. C_2 was in effect a short circuit at the resonant frequency of the loop, which was about 100 megacycles.

The contacts were operated mechanically, as shown in Figure 2. The moving contact arm was pivoted at one end and actuated by a cam at the other. The arm rested on the rotating cam for a half revolution and on the fixed contact for the remaining half. The static contact force was the portion of the weight of the arm borne by the lower contact. The test contacts were cylinders 0.125 inch long and 0.050 inch in diameter and were held by clamps which permitted easy removal for weighing. Contact was made between slightly rounded, metallographically polished end faces.

Precautions were taken to prevent undesirable mechanical effects. Chatter on the make was nullified by means of a butyl rubber pad beneath the fixed contact clamp. Horizontal play, which might have caused wiping was minimized by using tightly-fitting pivot bearings for the pivot shaft of the moving arm. Vibrations from the motor were blocked by suspending the motor on rubber mountings and driving the cam shaft through a sponge rubber fitting.

Unless otherwise indicated all tests were performed under the following conditions:

1. Battery voltage, 6.3 volts.
2. Steady current, 5.3 amperes.
3. Number of operations, 85,000.
4. Operation, make and break.
5. Frequency, 4 cycles per second.
6. Static contact force, 100 grams.
7. Atmosphere, room air.

Emphasis was placed on noble contact metals and alloys because of their importance in communications equipment. Among the materials which were studied are:

Platinum
Palladium
Gold
Silver
Platinum—iridium alloys
Palladium—silver alloys
Palladium—gold alloys
Palladium 60—copper 40
Gold 70—silver 30
Gold 69—silver 25—platinum 6
Gold 95—nickel 5

Base metals such as copper or nickel could not be operated at low contact forces under these test conditions, as oxidation at the high temperatures of the molten bridge soon resulted in open circuits.

Experimental Findings

The Molten Bridge

The formation and rupture of a molten metallic bridge between separating contacts are described here because much of what follows pertains to measurements on the remains of such bridges in the areas of erosion. This picture is based upon results of earlier investigators,[1–3] upon a recent detailed study by Lander and Ger-

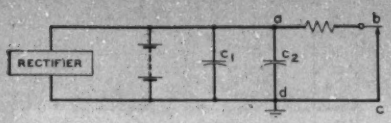


Figure 1. Testing circuit. $C_1 = 375$ microfarads. $C_2 = 0.1$ microfarad (mica)

mer,[4] and upon the writer's microscopic observations of ruptured bridges.

Consider two separating contacts: As the force is released the area of contact decreases; the resistance, and hence the temperature rises. Melting occurs at the interface, and liquid metal bridges the gap between the separating contacts. Further separation increases the resistance of the bridge until its temperature rises to the boiling point, whereupon a semi-explosive evolution of vapor blows the liquid metal into an irregular ridge about the base of the bridge in each contact. Figure 3 show stages in this process. At the instant of break the potential difference across the bridge is on the order of one volt.

Each explosion leaves two smooth, shallow pits, one in each contact, as shown in Figure 4. The eroded areas of both contacts become covered with these pits after many operations. The diameters of such pits have been carefully measured at high magnification and are the "bridge diameters" to which reference will be made later. An area typical of those in which such measurements have been made is shown in Figure 5.

Bridge Diameters

The diameter of the molten bridge increases linearly with current. Data for platinum contacts are shown in Figure 6. The plotted diameters are means of from 12 to 20 observations on both contacts. The bridge diameters in the two contacts were equal at all currents except 0.8 ampere.

The diameter of the molten bridge depends upon the contact material. Some values are shown in Table I. The figures are means of observations on both contacts.

The materials are listed in order of decreasing bridge diameter, which is also approximately the order of their resistivities at room temperature.

Although some metals form bridges which have equal diameters in the two contacts, in other cases the bridge may be asymmetric, and the diameters in the two contacts may differ considerably. Platinum is an example of the former; silver of the latter. In all like pairs which were examined and which showed asymmetry, the larger diameter was in the positive contact. Some values are listed in Table II. The asymmetry factor, f, is the ratio of the bridge diameter in the positive contact to that in the negative.

Asymmetry occurs only in the smaller bridges. Silver exhibits the greatest asymmetry and also has the smallest mean bridge diameter. If the bridge diameter is reduced by lowering the current, asymmetry appears for metals which bridge symmetrically at 5.3 amperes. For example, platinum, palladium, and palladium-silver alloys form asymmetric bridges at 0.80 ampere. Apparently the effect is masked in the larger bridges.

Transfer Tests

In every case material was transferred to the negative contact with negligible net loss. Buildup and crater fitted almost exactly. Anode losses varied from about 5×10^{-6} to 100×10^{-6} grams.

Transfer occurred only in separating contacts; under these conditions the erosion during closure was negligible.

The quantity transferred varies approximately as the third power of the current at currents in the neighborhood of 5 amperes.

The fraction of the volume of the bridge which is transferred to the negative contact at each operation is greater for asymmetric bridges than for symmetric ones. Bridge volumes were estimated from measured bridge diameters. The volume of metal transferred per operation was obtained from the transfer tests.

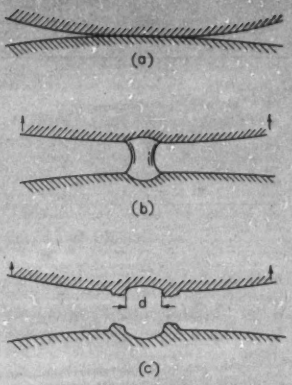


Figure 3. Separation of current-carrying contacts: (a) contacts closed, (b) formation of molten bridge, (c) contacts opened, d is the diameter of the molten bridge just before rupture

Analysis of Results

The data indicate that contact materials may be divided into two classes, symmetric and asymmetric, according to the form of the bridge and that greater transfer is shown by the asymmetric class. This suggests that at least two actions may cause transfer under these conditions, as the following analysis indicates.

Transfer of the first kind is apparent in metals which form symmetric bridges. Because the bridge is symmetric, one might expect that statistically each contact would contribute metal equally to the growing bridge and that upon rupture of the bridge, the scattered metal would divide equally between the two contacts. However, since transfer is observed, its origin is presumably unrelated to bridge shape. Erosion of this kind will be called A transfer. It was later found to be at least partly due to the discharge of inductive energy stored in loop *abcd* (see Figure 1) but this will not be discussed here. Under these test conditions, the

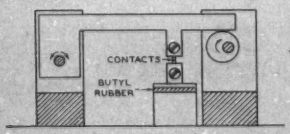


Figure 2. Make and break mechanism

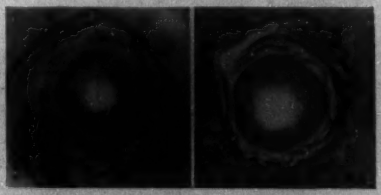


Figure 4. Corresponding craters left in positive and negative contacts after rupture of a molten bridge. Platinum contacts. 5.3 amperes. Magnification—4,500x

Table I. Mean Bridge Diameters at Steady Current of 5.3 Amperes

Material	Bridge Diameter (Centimeter × 10^4)
Pt 80–Ir 20	15.0
Pt	10.2
Pd	9.9
Au 69–Ag 25–Pt 6	9.7
Au 70–Ag 30	9.4
Au	4.2
Ag	3.0

Pt: platinum Ir: iridium Pd: palladium
Au: gold Ag: silver

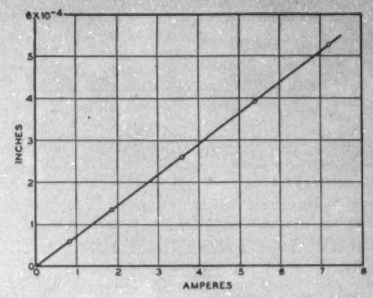


Figure 6. Diameter of molten bridge as a function of steady current for platinum contacts. Battery voltage—6.3

direction of A was always toward the negative.

The increased erosion observed for metals which form asymmetric bridges suggests that a transfer tendency which is related to the unbalanced form of the bridge reinforces A. Erosion due to this condition will be called B, or asymmetry transfer. Presumably the contact which has the larger bridge diameter contributes more metal to the bridge than does its mate, and then suffers a net loss in the division of the molten metal upon explosion of the bridge. Since for all like pairs showing asymmetry, the large bridge diameter has been in the positive contact, the directions of A and B have been the same.

To summarize, the data suggest that two kinds of transfer action may exist under these test conditions:

1. A, the direction of which is always toward the negative.

2. B, which is the result of asymmetry in the molten bridge. Its direction is away from the metal of large bridge diameter, and its magnitude increases with the degree of asymmetry.

Having postulated these two transfer actions, it became of interest to consider means of varying one of them independently. This led to the idea of creating an artificial asymmetry in the molten bridge by pairing metals having different natural bridge diameters. (By natural diameter is meant the bridge diameter when both contacts are of the same metal.) Since the sense of the asymmetry could be reversed by interchanging the contacts, B could be made either to oppose or to support A.

The erosion to be expected for such a combination will be discussed, assuming platinum and gold as the two metals. Platinum has the larger natural bridge diameter and forms symmetric bridges; gold has a natural asymmetry which may be disregarded here. In Figure 7 the shapes of the molten bridge are shown schematically and the direction and magnitude of A and B are represented by vectors for both arrangements of the contacts. According to the analysis the erosion will be as follows:

Case I. *Platinum* (+), *Gold* (−): Since platinum has the larger bridge diameter, A and B act in the same direction, and transfer begins toward the negative at a high rate, as shown in Figure 7 (*a*). As the layer of platinum builds up on the negative contact, that contact becomes more like platinum. Hence the bridge diameter in the negative gradually increases until at some layer thickness the underlying gold no longer has an effect and the bridge diameter attains the value for platinum. This situation is shown in (*b*). The asymmetry has disappeared and transfer toward the negative continues at the normal rate for platinum.

Case II. *Gold* (+), *Platinum* (−): A and B are opposed, because the larger bridge diameter is now in the negative contact, as shown in (*c*). Since B has been taken greater than A, transfer begins toward the gold contact. Notice that the direction of transfer is now toward the positive. The layer of platinum on the gold contact causes an increase in bridge diameter in that contact. Hence B decreases, because of the reduction in asymmetry, until it becomes equal to A, as shown in (*d*). This is the equilibrium condition for the system. No further transfer occurs.

If in some way a thicker layer of platinum had formed on the gold, the bridge asymmetry would then be so small that B would be less than A as shown in (*e*). A transfer of platinum from the layer back to the negative contact would begin and would continue until the system was returned to equilibrium.

If B were taken less than A initially, transfer would not be toward the positive, but instead would begin at a low rate toward the negative. As the metal of the positive contact built up on the negative, the asymmetry would gradually disappear and the rate of transfer would increase to the value for like contacts of the metal of the positive contact.

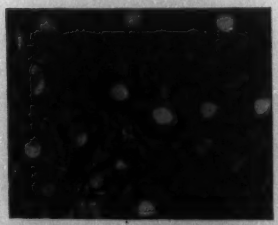


Figure 5. Craters in eroded region of contacts of Pd–40 per cent Ag. 5.3 amperes. Magnification—1,500x

Table II. Bridge Diameters in Positive and Negative Contacts at 5.3 Amperes

Material	Diameter in (+) (Centimeter × 10^{-4})	Diameter in (−) (Centimeter × 10^{-4})	f	Bridge Form
Pt80–Ir20	15.2	14.8	1.0	Symmetric
Pd60–Ag40	12.7	12.7	1.0	
Pt	10.2	10.2	1.0	
Pd	9.9	9.9	1.0	
Au69–Ag25–Pt6	9.9	9.4	1.0	Asymmetric
Au70–Ag30	9.4	6.6	1.4	
Pd20–Ag80	9.4	6.4	1.5	
Au	5.7	3.0	1.9	
Ag	4.3	1.8	2.4	

Table III. Height of Buildup for Various Combinations

Number	Positive Contact	Negative Contact	Transfer was Toward:	Height of Buildup (Centimeter ×10⁶)
1	Pd	Pd	−	1.0
2	Pt	Pt	−	2.5
3	Pt-20Ir	Pt-20Ir	−	4.8
4	Au	Au	−	5.6
5	Ag	Ag	−	8.1
6	Pt	Au	−	3.0
	Au	Pt	+	< 0.2
7	Pt	Ag	−	3.2
	Ag	Pt	+	< 0.2
8	Pt-20Ir	Au	−	6.1
	Au	Pt-20Ir	+	< 0.2
9	Pd	Au	−	2.3
	Au	Pd	+	0.8
10	Pd	Ag	−	2.3
	Ag	Pd	+	0.5
11	Pt-20Ir	Pt	−	5.8
	Pt	Pt-20Ir	−	1.3
12	Pt	Pd	−	1.8
	Pd	Pt	+	< 0.2
13	Au	Ag	−	5.6
	Ag	Au	−	6.3
14	Pt-20Ir*	Au	−	27.0
	Au**	Pt-20Ir	+	< 0.2

*1,000,000 operations.
**2,066,000 operations.

Confirmation of the Analysis

The analysis indicates that continued transfer of contact material under these test conditions can be prevented by forming an "equilibrium layer" on the positive contact which equalizes A and B and thereby prevents further change. Confirmation of this conclusion was obtained from erosion tests whose results appear in Table III. The tests were performed under the standard conditions previously listed.

In Table III the height of the buildup has been used as a measure of transfer. Rows 1 to 5 show the transfer for like pairs of five contact materials. The natural bridge diameters for these materials were given in Table I; for platinum, palladium, and platinum +20 iridium they are large in comparison with those of silver and gold. Hence a combination of any one of these with silver or gold may be expected to exhibit strong asymmetry transfer.

Rows 6 to 10 illustrate the formation of an equilibrium layer on the positive contact when the metal of larger bridge diameter is negative. For platinum against gold, platinum against silver, and platinum + 20 iridium against gold the layer is very thin. For palladium against gold and palladium against silver, it is of measurable thickness. Also, when the metal of larger bridge diameter is positive, the buildup is somewhat larger than for like contacts of that metal. This is presumably due to the high initial rate of transfer indicated in Case I.

In row 11, with platinum +20 iridium negative and platinum positive, the asymmetry was insufficient to prevent transfer to the negative but merely reduced the initial rate. This corresponds to B less than A in Case II. Hence both arrangements of the contacts resulted in transfer to the negative, although at different initial rates.

In row 12, an equilibrium layer formed on palladium when platinum was negative. This was unexpected in view of the near equality of bridge diameters for these metals.

In row 13, transfer was to the negative in both cases. This was expected because of the lack of a great difference in bridge diameter between gold and silver. Also, the strong natural asymmetry of these metals in like pairs appears to be present in their combinations.

How effectively transfer is prevented once equilibrium has been reached is shown for platinum +20 iridium against gold, in row 14. Increasing the number of operations by a factor of 24 caused no further change, aside from a spreading of the area of erosion, when the platinum +20 iridium was negative. A huge buildup resulted in half this number of operations when the platinum +20 iridium was positive. Micrographs of the test contacts are shown in Figure 8.

With the exception of the platinum-palladium pair, the prediction of these results by the analysis was surprisingly accurate, especially since factors such as alloying, differences in melting and boiling points, and thermoelectric effects were not considered in the analysis.

In considering the molten bridge, a distinction must be made between the asymmetry to be expected of like contacts and that of a combination of two materials which differ considerably in natural bridge diameter. The former may be caused by Thompson or Peltier heating and has been assumed by some investigators[1,2,3] to be a cause of bridge erosion. The transfer caused by it is invariant for a given current, and should be classed with the A transfer forces for the purposes of this paper. On the other hand the artificial asymmetry of the combination has been shown to cause a high initial rate of transfer which is self-reducing.

When it was stated in the section on Transfer Tests, that asymmetric bridges in like pairs caused greater transfer than symmetric ones, it was tacitly assumed that the measured diameters were characteristic of the molten bridge alone. It was later found that even at the low inductance of this testing circuit electrical discharges may have occurred in the separating contacts, with resultant enlargement of the crater in the positive contact. Hence, the observed asymmetry which originally suggested the use of combinations may possibly, in certain cases, have been due to arcs, and the correlation of transfer results with asymmetry may have been partly coincidental. However, the finding which was suggested is a valid one, as shown by much experimental confirmation.

Scope and Applications

It has been shown that the formation of an asymmetric bridge by pairing electrical contacts which differ in natural bridge diameter creates a transfer process which is here called "asymmetry transfer" or B. This process is self-limiting and can nullify other transfer actions. It is self-limiting because the gaining electrode becomes similar to the losing electrode, thereby removing the driving force for the action. If no other action is present, asymmetry transfer will continue until both contacts become alike as far as the molten bridge is concerned. In the case of platinum against gold at five amperes, this occurs when the thickness of the layer of platinum on gold is on the order of 2 bridge diameters. If other transfer actions are present, the process will still be self-limiting if (1) B is greater than the resultant, A, of these actions and (2) is opposite to A in direction. Transfer will cease when B becomes equal to A. The thickness of the equilibrium layer will then depend on the relative magnitudes of B and A and will vary from about 2 bridge diameters when A equals zero, to zero when A equals B.

Thus, "self-limiting transfer" is said to exist when B is greater than A and the two are opposite in direction. Circuit conditions and practical arrangements of contacts for the attainment of self-limiting transfer will now be considered.

Transfer tests on platinum contacts at 6.3 volts and inductances up to 1×10^{-3} henry show that: (1) at currents below about 1 ampere short arcs cause negative transfer which increases with inductance, and (2) at larger currents increasing negative transfer occurs up to a critical inductance at which normal arcs begin. The normal arc causes positive transfer and is much more severe than the short arc. Each of these arc-transfers falls here into the A category.

It is of interest to examine the ranges of current and inductance for which the effects of arcs can be counteracted by the asymmetry transfer of a platinum-gold combination. These have been determined very approximately and are indi-

cated in Figure 9. The shaded area is the region of self-limiting transfer for the combination platinum (−) against gold (+) at 6.3 volts. The lower boundary is the locus of points at which short arc transfer and asymmetry transfer are equal and opposite. The upper boundary is the locus of points at which normal arcs begin. By reversing the combination, self-limiting transfer can be obtained a short distance beyond the upper boundary. Tests were made also at 50 volts and show that self-limiting transfer exists at least up to 1×10^{-8} henry at a current of one ampere.

An odd consequence of Figure 9 is that erosion of this combination, occurring under conditions below the lower boundary can be prevented by increasing the current.

The limits of Figure 9 are for the case of negligible capacity across the contacts ($\sim 10^{-11}$ farad). By placing a capacitor across the contacts the boundaries can be moved to considerably higher inductances. Also, the limits apply to contacts having opening velocities below certain maximum values, which have been determined by Lander and Germer[4] for one circuit of low inductance.

From known values of A for a pair of platinum contacts, it is possible to estimate the value of B for the platinum-gold combination. At 5.3 amperes, B is on the order of 5×10^{-11} cubic centimeter per operation.

For the present purpose contact materials may be classed as I or II depending on whether their relative natural bridge diameters are small or large, respectively. Any member of Class II may be used with any member of Class I to prevent the transfer of contact material under conditions of self-limiting transfer. Examples of materials which have been used successfully are gold, silver, copper, and molybdenum in Class I and platinum, palladium, platinum 80-iridium 20, palladium 60-silver 40, and gold 90-nickel 5 in Class II. While many other materials can be used for Class II, those for Class I may be limited in number. Small natural bridge diameters are favored by low electrical resistivity and not so many materials meet this requirement. Base metals can be used also, but if chosen as the Class II material an inert atmosphere may be necessary.

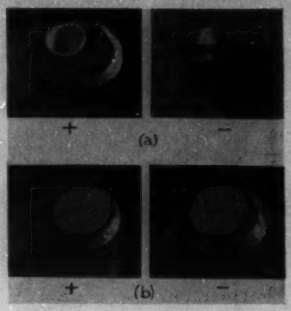


Figure 8. Erosion of the combination Pt–20Ir against Au at 5.3 amperes: (a) Pt–20Ir (+), Au (−), 1,000,000 operations, (b) Au (+) Pt–20Ir (−), 2,066,000 operations. Magnification—40x

Since self-limiting transfer requires that B be greater than A it is desirable to choose combinations of materials which enhance this difference. The following factors are determining:

1. Ratio of the natural bridge diameters of the Class II to Class I material. Available data indicate that B increases with this ratio. Relative melting and boiling points may also be pertinent.[4]

2. Current. The volume of the molten bridge, and hence B, increases rapidly with current.

3. The value of A for a like pair of the Class II material. As the bridge breaks, first B transfer occurs, depositing Class II material on the Class I contact. Then A transfer occurs, removing some of the deposited material. Hence of two different combinations having equal values of B, that having the lesser value of A for the Class II material will be more effective.

So far, self-limiting transfer has been

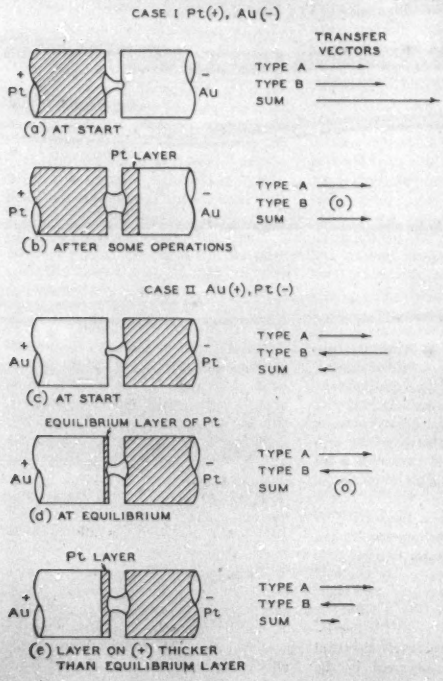
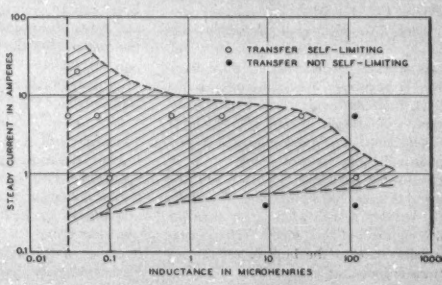


Figure 7 (left). Postulated bridge shapes and transfer vectors for the platinum-gold combination

Figure 9 (below). Region of self-limiting transfer for platinum (−), gold (+). Boundaries approximate



discussed in terms of a pair of dissimilar homogeneous contacts, one of Class I and the other of Class II. A more flexible arrangement, which is independent of polarity, utilizes duplex contacts. For example, by pairing two copper contacts each faced with a layer of platinum about two or more bridge diameters in thickness, self-limiting transfer can occur for either direction of A. Removal of platinum from either contact, decreases the bridge diameter in that contact and sets up an asymmetry which opposes the transfer. If the asymmetry is strong enough, self-limitation will occur.

It is interesting that a contact combination of a Class I base metal and a Class II noble metal can be used successfully under conditions which would rapidly oxidize the base metal. This is the case for platinum(−) against copper (+) at 6.3 volts, 5.3 amperes, and low inductance. Because of the equilibrium layer, contact is actually between two surfaces of platinum.

The formation of an equilibrium layer may be considered to advantage as a means of depositing a layer of Class II material on a Class I material. The layer is firmly bonded by fusion and is uniform in thickness, although rough on a microscopic scale.

The thickness can be varied from less than 2×10^{-4} centimeter to over 2×10^{-3} centimeter at 5.3 amperes by changing the inductance of the circuit and remains at a constant value once equilibrium has been reached. This technique would be useful for small areas not conveniently accessible by other means. For example, a disk or marking of platinum could be deposited on a flat sheet or curved surface of gold or copper.

Summary

Careful examination of contacts operated under conditions in which bridge erosion predominates shows that, despite a gross transfer of material from anode to cathode, the explosion of a molten bridge leaves a small crater in each contact. The diameters of such craters are believed to be those of the molten bridge just before rupture and they vary with current and with resistivity of the contact material in the expected manner.

Although for some materials the bridge has equal diameters in the two contacts, in other cases it is asymmetric, that is, the diameters in the two contacts may differ considerably. Results of bridge erosion tests suggest that a transfer of material in the direction of the smaller end of the bridge is associated with such asymmetry.

By pairing two materials which differ in natural bridge diameter, an artificial asymmetry is created. The transfer tendency of such a combination is called here asymmetry transfer and is shown to be self-limiting, because the act of transfer causes the gaining electrode to become similar to the losing electrode, thereby removing the driving force which causes the action.

The concept of self-limiting transfer is developed, whereby it is shown that a condition of zero net transfer will result if contacts are operated under conditions so that, (a) asymmetry transfer is greater than the resultant of other transfer forces acting on the contacts, and (b) is opposite in direction to that resultant. Because of the large values which asymmetry transfer has for certain combinations, the transfer of contact material can be prevented not only under conditions in which bridge erosion predominates but also, to some extent, in the presence of arcs.

Contact materials may be classed as I or II depending on whether their natural bridge diameters are small or large, respectively. Any member of Class I may be used with any member of Class II to establish a condition of self-limiting transfer. Instead of two homogeneous contacts, one of each class, it is equally effective to use two similar duplex contacts, each having a thin Class II layer on a Class I base. Such a configuration is independent of polarity or change in polarity and may be used to conserve precious metals.

Since a thin, Class II layer whose thickness is invariant with time is formed on the Class I base at equilibrium, the principles described here suggest a means of coating one metal on another in a well-bonded layer of uniform but controllable thickness.

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No Discussion

The Magnetic Properties of Stainless Steels

WILLIAM A. STEIN
ASSOCIATE AIEE

Synopsis: This paper presents the results of an extensive investigation of the magnetic properties of solid stainless steel specimens. The analysis includes direct and alternating current magnetization curves, hysteresis loops, eddy current losses, and hysteresis losses. These tests were performed over a temperature range of −70 degrees to 600 degrees Fahrenheit. A discussion of the difficulties that might be encountered using stainless steel in magnetic circuits is included.

STEELS containing varying amounts of nickel and chromium have been found to be resistant to chemical corrosion and oxidation, and for this reason are grouped under the general name of stainless steel. Little thought was given to their adaptability in electromagnetic circuits, as it was a generally accepted fact that most of the nickel-chromium steels were nonmagnetic or relatively poor in magnetic characteristics as compared with silicon steel. However, the manufacturers of solenoid valves encountered so many valve failures in attempting to use regular steel in valves exposed to acid vapors, that they were forced to resort to the magnetically inferior stainless steels. As this is a highly specialized use of stainless steels, it was not surprising that none of the steel companies had available data on their magnetic properties.

In order to avoid excessive fabrication cost, the valve manufacturers had been using solid stainless steel sections instead of laminated pieces. This meant a large percentage of the over-all heat losses in the valve under alternating current conditions would be due to eddy currents. Without any data indicating the magnitude of this eddy current loss, the valve designers had no way of knowing whether the reduction of losses by laminating would offset the additional cost. This meant that the test specimens for this investigation had to be of about the same cross sectional size as the finished valve piece; approximately a one-half inch square.

Testing Difficulties

Testing solid specimens with alternating current in itself presented a unique problem in equipment design and laboratory procedure. It was impossible to find much information in any previously published literature concerning this technique, since most a-c tests are run on laminated specimens. This is due to the fact that data obtained on a specimen of a certain cross sectional area using alternating currents would not be applicable, except in a qualitative sense, to a specimen of the same material but of different area. This limitation is attributable to the fact that the magnetic flux does not completely penetrate the specimen, and eddy currents are of such proportions that they distort the magnetic properties of the steel.

Since stainless steel has poor magnetic characteristics, it was essential that the testing apparatus should be capable of producing unusually high magnetomotive forces. Another important requirement was that the apparatus should be built to withstand temperatures of 600 degrees Fahrenheit.

Since solid specimens were used, it meant that the ends of the bars could not be butt lapped as with laminated specimens. In order to reduce the air gap between the bars, the ends were polished and clamped together by auxiliary pieces of soft steel.

The Stainless Steel Specimens

Six different types of the more common stainless steels were obtained for this research from the Carpenter Steel Company of Reading, Pa. A special analysis of the chemical content of each, and an accurate record of all heat treatments was made. The important data on these six specimens of Carpenter stainless steel is shown in Tables I and II.

D-C Tests

D-c normal magnetization and permeability curves, Figure 1, were obtained for the six specimens up to 1,500 ampere-turns per inch. The first portion of the curve from 10 to about 300 ampere-turns per inch was obtained by a modified Epstein tester; the upper portion of the curve was obtained by a special permeameter. It was possible to check these curves up to 700 ampere-turns per inch by means of a General Electric permeameter[1] type *LC*, form *C*. This permeameter had been adopted by the Bureau of Standards and the American Society for Testing Materials as a standard method of test. The results of the General Electric permeameter agreed with those of the modified Epstein tester and special permeameter within 2 or 3 per cent.

Both the modified Epstein tester and the special permeameter were made of heat resisting materials. The coils were wound with glass insulated wire on mykroy forms, impregnated with Dow Corning 993. The supporting frames were of brass mounted on an asbestos base. With this special equipment it was possible to vary the temperature from −70 to 600 degrees Fahrenheit.

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WILLIAM A. STEIN is with the department of electrical engineering, Washington University, St. Louis, Mo.

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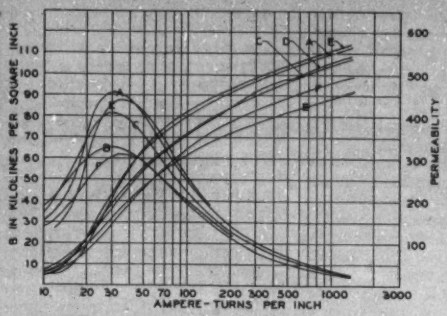

Figure 1. D-C normal magnetization and permeability curves for all six specimens tested at room temperature

$$\text{Permeability} = \frac{0.313 \text{ kilolines per square inch}}{\text{ampere turns per inch}}$$

In Figure 2, the d-c normal magnetization and permeability curves for specimen *A* are shown for various temperatures. The data at −70 degrees Fahrenheit coincided so closely with that at room temperature, that it was omitted from the figure.

The hysteresis loops of three of the specimens are shown in Figure 3. The residual magnetism of the steels are high; hence, special care must be taken that the valves do not stick magnetically. It is also quite evident that the hysteresis losses of stainless steel parts are quite large compared to ordinary steels.

A-C Tests

The a-c magnetization curves were, in most cases, plotted to about 500 maximum ampere turns per inch. In some cases, however, it was found that the sample being tested, heated too fast due to core losses at high flux densities to take accurate readings. For this reason, some of the curves in Figure 4 were not carried to 500 maximum ampere turns per inch.

In order to measure the maximum ampere turns per inch, it was necessary to obtain the peak current. The current to be measured was sent through a standard resistance, and a maximum reading vacuum tube voltmeter connected across the standard resistance indicated the maximum *IR* drop.

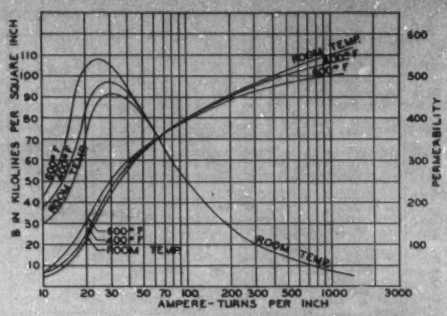

Figure 2. D-C normal magnetization and permeability curves for specimen A tested at room temperature, 400, and 600 degrees Fahrenheit

$$\text{Permeability} = \frac{0.313 \text{ kilolines per square inch}}{\text{ampere turns per inch}}$$

It will be noted that the vertical ordinate is plotted as maximum flux density in lines per square inch. This was calculated from the formula:

$$B_M = \frac{E_{AV} \times 10^4}{4NfA}$$

E_{AV} was the average voltage read across the search coil by a vacuum tube voltmeter. It has been proved by Camilli[2] and others that the maximum flux density is proportional to the average voltage regardless of the wave shape of the voltage, provided the wave did not cross the zero line more than once per cycle.

The total core losses per pound at varying temperatures and at frequencies of 25 and 60 cycles were determined for all six specimens. These core losses were run

Table I. Resistivity and Chemical Analysis of Specimens

Specimen Number	Type Number	Specific Resistivity (Ohms Per cir-mil. ft.)	Chemical Analysis C	Mn	Si	P	S	Cr	Ni	Mo	Cu
A	416	382.5	0.09%	0.42%	0.56%	0.016%	0.286%	13.22%			
B	446	398	0.09	0.61	0.50	0.015	0.010	25.85			
C	430F	376.5	0.07	0.42	0.43	0.009	0.365	17.10		0.34	
D	430	373	0.10	0.46	0.44	0.025	0.009	16.95			
E	410	341.5	0.13	0.41	0.30	0.015	0.008	12.37	0.50		
F	443	420	0.11	0.38	0.35	0.015	0.020	21.05			1.18

Table II. Brinell Hardness and Heat Treatment of Specimens

Specimen Number	Type Number	Brinell Hardness Number	Heat Treatment
A	416	163	Annealed by cooling slowly from a temperature of 1,380 degrees Fahrenheit (750 degrees centigrade)
B	446	163–179	Water treated from a temperature of 1,500 degrees Fahrenheit (815 degrees centigrade)
C	430F	170	Water quenched from a temperature of 1,240 degrees Fahrenheit (670 degrees centigrade)
D	430	163–170	Water quenched from a temperature of 1,500 degrees Fahrenheit (815 degrees centigrade)
E	410	187	Annealed by cooling slowly from a temperature of 1,335 degrees Fahrenheit (725 degrees centigrade)
F	443	163	Water treated from a temperature of 1,500 degrees Fahrenheit (815 degrees centigrade)

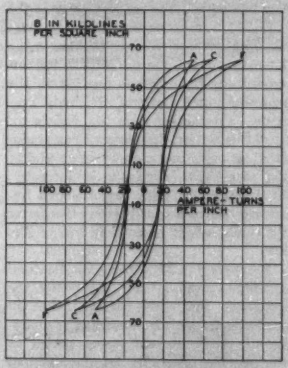

Figure 3. Hysteresis loops of specimens A, C, and F tested at room temperature

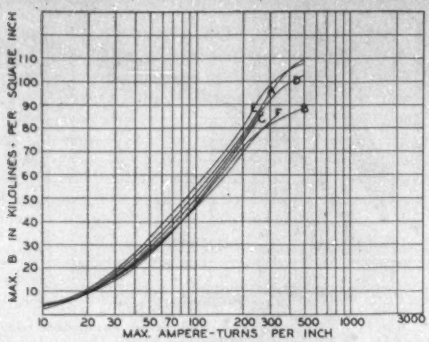

Figure 4. 60-cycle a-c normal magnetization curves for all six specimens tested at room temperature

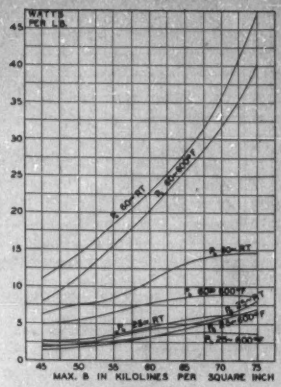

Figure 6. Eddy current and hysteresis losses per pound of specimen A tested at room temperature and 600 degrees Fahrenheit, and at 25 and 60 cycles

on a sine wave basis. If the form factor differed from 1.11 by more than 2 per cent, a correction was applied.[3] Figure 5 shows the total core losses in watts per pound at 60 cycles tested at ambient temperature.

The core losses for each specimen were divided into their component parts and plotted against maximum flux density. Such a curve is shown in Figure 6 for specimen number *A*. It is quite apparent that the eddy current losses at 60 cycles are intolerable. At 25 cycles the eddy current losses are of the same relative magnitude as the hysteresis losses. These curves clearly show the effect of temperature on both hysteresis and eddy current losses.

According to Steinmetz[4,5] alternating flux does not completely penetrate a solid specimen due to the screening effect of the eddy currents. The magnetic flux in the interior of the steel specimen is not in phase with the flux at the surface, but lags behind it. The magnetic flux at the surface is caused by the impressed magnetomotive force, whereas the flux in the interior is due to the resultant of the impressed magnetomotive force and the magnetomotive force of the eddy currents. Progressing from the surface toward the interior, the magnetic flux gradually lags more and more in phase and at the same time decreases in intensity. In the case of a thick bar or a high frequency alternating magnetomotive force, the center might be entirely free of flux lines.

Rosenberg[6] and other investigators[7] developed formulae for separating the hysteresis and eddy current losses in solid specimens. In Rosenberg's analysis the eddy current losses varied as the specific resistivity to the one-half power and as the magnetomotive force to the two-thirds power. Unsuccessful attempts were made to use these formulae with the data of this experiment.

In these tests it was only possible to determine the average value of the maximum flux densities in the bar, as the flux density was obtained by dividing the average electromotive force of the search coil wrapped around the specimen by $4\ ANf\times10^{-8}$. It was found that the plotting of $(P_h+P_e)/f$ versus f for a particular average value of the maximum flux densities produced a straight line graph. This meant that the hysteresis losses varied directly as the frequency and the eddy current losses varied directly as the frequency squared. The losses were then separated by the usual method of holding the average value of the maximum flux densities in the specimen constant and varying the frequency.[8] Special care was taken not to vary the frequency over too wide a range, as flux distribution is a function of frequency. Precautions were also taken to prevent the core losses from heating the specimen. Readings had to be made quickly and a cooling off period was allowed between successive readings. Figure 6 shows the hysteresis loss as staying essentially constant from about 67 to 75 kilolines per inch. This discrepancy indicates that the separation was not too accurate in the higher ranges of flux densities; however, the results do show the relative magnitudes of the individual losses.

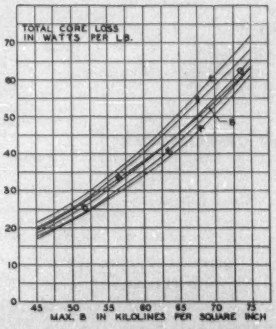

Figure 5. Total core losses per pound for all six specimens tested at 60 cycles and at room temperature

Effect of Nickel Content in Nickel-Chromium Steels

In order to determine the effect of nickel on the magnetic properties of steel, similar tests were conducted on some nickel-chromium steels. Among those tested were an Allegheny Ludlum type *414* which contained 12 per cent chromium and 2.5 per cent nickel and an Allegheny Ludlum type *431* which contained 16 per cent chromium and 2.5 per cent nickel. The curves from these tests are not shown in this report. However, it was found that these steels compared favorably with the Carpenter steels previously examined.

Attempts were made to run tests on nickel-chromium steels containing 8, 10, 12, and 20 per cent nickel. However, it was impossible to detect any magnetism with the testing equipment.

Conclusions

From the results of these tests and similar experiments, it is to be concluded

that the smaller the amount of chromium present, the more magnetic the stainless steel will be. Those steels with the low chromium content of about 12 or 13 per cent also had a much higher residual magnetism. An examination of Figure 5 will show that the low chromium stainless steels, specimens *A* and *E*, had a lower core loss per pound than the other types. Steels can tolerate up to 2 or 3 per cent nickel without serious detrimental effects on their magnetic characteristics. Steels having in excess of 8 per cent nickel are nonmagnetic; and can be used for valve parts subjected to corrosion and oxidation, but which are not included in the magnetic circuit.

As seen from the hysteresis loops, the product of the residual magnetism and the coercive force is considerable, and hence stainless steels make fair permanent magnets. In order to overcome this permanent magnetism it was necessary to insert a small spring in the solenoid valve opposing the electromagnetic action.

When the energizing current is shut off, the spring overcomes the residual magnetism and snaps the plunger back into its normal position.

After examining the eddy current losses of the solid specimens, such as in Figure 6 which shows the losses per pound of specimen *A*, it becomes apparent that a laminated plunger although more expensive is quite necessary. Otherwise, the eddy current losses in the plunger would be so great, that heat would be radiated into the solenoid coil of the valve and cause an excessive temperature rise.

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No Discussion

Proximity Effect Factors for 3-Phase Coaxial Busses Comprised of Square Tubular Conductors

THOMAS JAMES HIGGINS
MEMBER AIEE

HENRY P. MESSINGER
NONMEMBER AIEE

THE THEORY underlying design of 3-phase coaxial busses comprised of square tubular conductors, Figure 1, has been developed by us in a recent paper.[1] In section VIII of this paper:

"It is determined that the proximity effect factors of a given bus of coaxial square tubular conductors are essentially equal to the corresponding factors of a "similar" bus comprised of circular tubular conductors, Figure 2, each of which has external diameter and thickness respectively equal to the external side and thickness of the corresponding square conductor.

"It is pointed out that in consequence of this equality the proximity effect factors can be calculated through use of Dwight's[2] curves (Figure 3 is a partial reproduction of these curves) for the ratios R_{ac}/R_{dc} of the conductors of a 3-phase coaxial bus comprised of circular conductors."

This calculation is to be effected as follows. Corresponding to a given bus comprised of square conductors, effect the "similar" bus of circular conductors. From well-known theory, a circular tubular conductor carrying a current, of which the density can vary radially but not angularly over the cross section, produces no flux within the tube. Accordingly, the metal of the inner conductor of a coaxial bus comprised of circular tubular conductors lies in a field produced only by the conductor itself. Thus the proximity effect factor K_p of the inner conductor is one; whence the ratio $R_{ac}/R_{dc}=K_sK_p$ of the inner conductor is equal to the skin effect factor K_s.

In turn, as skin effect factor depends only on self geometry, the appropriate curve of Figure 3 for the inner conductor also yields the skin effect factor of the intermediate conductor. Then if the value of R_{ac}/R_{dc} for the intermediate conductor is divided by this skin effect factor, the proximity effect factor of the intermediate conductor is obtained. Obviously, the proximity effect factor of the outer conductor is to be obtained in the same fashion. But, inasmuch as the proximity effect factors of the given bus and of the corresponding "similar" bus are the same, it follows that the desired proximity effect factors of the given bus have been obtained.

In illustration of this calculation consider the following example: to calculate the proximity effect factor for an intermediate square conductor having standard dimensions[3] of edge $c=10$ inches and thickness $t=(c-d)/2=0.5$ inch. From Figure 3 the skin effect factor for the "similar" circular conductor of ratio $t/D=0.5/10=0.05$ is $K_s=1.25$ (taken from the 0.05 curve for inner tube); the ratio R_{ac}/R_{dc} is 1.285 (taken from the 0.05 curve for outer tube). Hence, the proximity effect factor is $K_p=1.285/1.25=1.028$.

To obviate need of calculation and to render better accuracy the curves of Figure 4 have been plotted from points with co-ordinates calculated as in the first example. From these curves can be obtained by inspection the proximity effect factors of the intermediate and outer conductors (that of the inner conductor is 1) of a 3-phase coaxial bus comprised of square tubular conductors. Illustratively, the 0.05 curve for outer conductor yields the value $K_s=1.03$ for the conductor of the first example.

To determine the co-ordinates of Figure 4, a photograph of the original cut (Figure 2 of reference 2) of Dwight's curve was enlarged about three times. From this enlargment, and aided by the fact that the curves were plotted on a grid of fine mesh, the co-ordinates of points on the curves could be obtained with considerable accuracy. From these values were calculated the co-ordinates of the curves of Figure 4. The two sets of co-ordinates are contained in Tables I

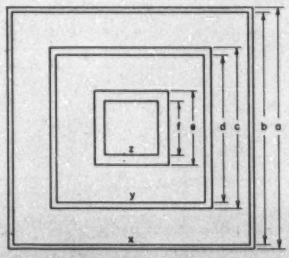

Figure 1. Cross section of 3-phase coaxial bus comprised of square tubular conductors

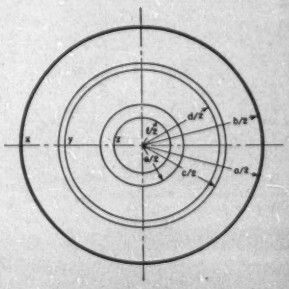

Figure 2. Cross section of "similar" bus comprised of circular tubular conductors

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THOMAS JAMES HIGGINS is professor of electrical engineering, University of Wisconsin, Madison, Wis., and HENRY P. MESSINGER is a graduate student, department of electrical engineering, University of Illinois, Urbana, Ill.

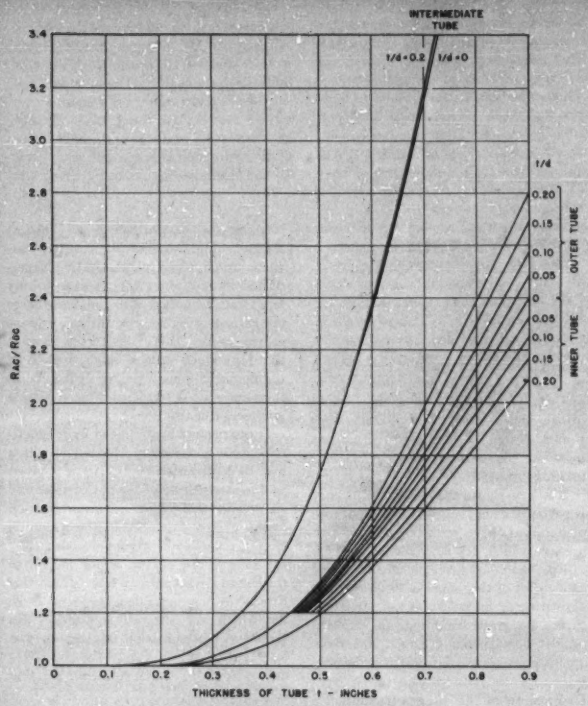

Figure 3 (above). Curves of R_{ac}/R_{dc} for "similar" bus

Figure 4. Curves of proximity effect factor for square tubular conductors

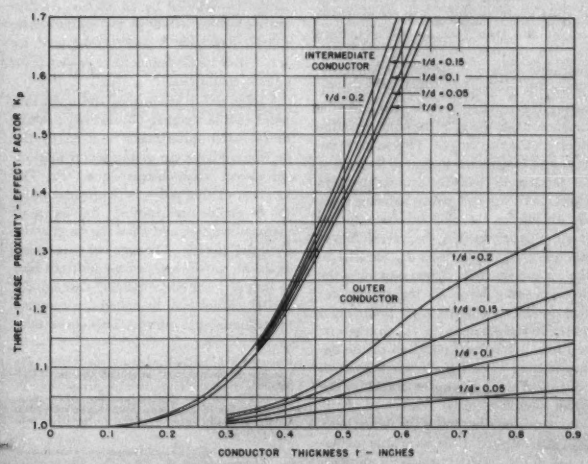

Table I. Values of K_p for Outer Conductor

	Values of t/D				
t	0	0.05	0.1	0.15	0.2
0	1.0	1.0	1.0	1.0	1.0
0.1	1.0	1.0	1.0	1.0	1.0
0.2	1.0	1.0	1.0	1.0	1.0
0.3	1.0	1.00	1.0	1.015	1.02
0.4	1.0	1.018	1.028	1.038	1.045
0.5	1.0	1.028	1.058	1.073	1.095
0.6	1.0	1.035	1.072	1.125	1.178
0.7	1.0	1.043	1.094	1.158	1.245
0.8	1.0	1.057	1.122	1.20	1.295
0.9	1.0	1.062	1.142	1.235	1.34

Table II. Values of K_p for Intermediate Conductor*

	Values of t/D				
t	0	0.05	0.1	0.15	0.2
0	1.00	1.00	1.00	1.00	1.00
0.1	1.00	1.00	1.00	1.00	1.00
0.2	1.02	1.02	1.02	1.02	1.02
0.3	1.072	1.072	1.072	1.082	1.083
0.4	1.192	1.20	1.205	1.215	1.221
0.5	1.38	1.395	1.42	1.43	1.449
0.6	1.585	1.615	1.65	1.690	1.73
0.7	1.781	1.83	1.875	1.945	2.01

*The values for $t/D = 0.05$, 0.1 and 0.15 were obtained by linear interpolation from those for $t/D = 0$ and 0.2.

and II. They enable the interested designer to plot to a larger scale and on a finer grid the curves of Figure 4. As regards Figure 3, the original curves (Figure 2 of reference 2) extend to smaller values of t, are plotted on a grid with mesh one-fifth the size of that of Figure 3, and render values sufficiently accurate for engineering purposes (the values of K_s in example 1 were taken from the original curves).

It is to be mentioned that the curves of Figures 3 and 4 are for 60-cycle balanced currents with relative phase angles $\theta = 0$ degrees, $\theta = 120$ degrees, and $\theta = 240$ degrees. However, essentially the same values are associated with opposite phase sequence; again values of K_s and K_p corresponding to any other frequency are to be obtained by multiplying the actual conductor thickness by $(f/60)^{1/2}$ and then using the curves as above.

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No Discussion

Stabilized Permanent Magnets

P. P. CIOFFI
MEMBER AIEE

PERMANENT magnets are stabilized against forces tending to demagnetize them, by partial demagnetization. It is shown that after such stabilization, the magnet operates at a point on a secondary demagnetization curve. This curve may be treated identically as the major demagnetization curve is treated in ordinary magnet design problems. Formulas are developed for determining secondary demagnetization curves from the major demagnetization curve when stabilization is achieved by magnetization of the magnet before assembly, and by an applied magnetomotive force after magnetization in assembly.

Since the development of permanent magnet alloys with improved characteristics, rapid advances have been made in the application of permanent magnets. Alloys of the Alnico type, characterized by high coercive force and energy-product, have made it possible to generate high stable fields in air-gaps of large volume more economically than with electromagnets. The degree of utilization of the energy furnished by the permanent magnet is dependent, however, upon the particular application. In applications where the magnet is not subjected to significant demagnetizing forces, such as in magnetic circuits for loudspeakers, light valves, and magnetrons, the magnet can be designed to operate at its maximum energy point on the demagnetization curve. However, in applications where the magnet is subjected to partial demagnetization because of applied demagnetizing forces, the necessity for magnetization before assembly or because of operation in a circuit of variable reluctance, the magnet operates at a point within the demagnetization curve quadrant, and the magnet delivers less energy to the air-gap. Examples of such applications are receivers, generators, and polarized relays. Under this condition of operation, a degree of stabilization is obtained which makes it necessary to use a larger magnet volume than would otherwise be needed. The problem of optimum design of stabilized permanent magnets has been studied by several investigators.[1,2,3]

It will be shown that when the magnet is partially demagnetized for the purpose of stabilization, its operating point lies on a curve which, for convenience, will be called a secondary demagnetization curve. The object of this paper is to discuss the derivation of secondary demagnetization curves for given conditions of stability against demagnetizing forces and their applications to magnet design problems.

Discussion of the Magnetization Cycle

Let us assume that the magnet is magnetized while asssembled in its magnetic circuit and then partially demagnetized for the purpose of stabilization. The magnetic cycle is illustrated in Figure 1. When the magnetizing force H_s for saturating the magnet to B_s is removed, the magnetization drops to the point P on the demagnetization curve. This point is determined by the intersection of the load or air-gap line OP with the demagnetization curve. An applied demagnetizing force H_d diminishes the magnetization to the point Q, determined by the intersection of the line QH_d drawn from H_d parallel to the air-gap line OP. Upon removing the demagnetizing force H_d the magnetization proceeds along the lower branch of a minor hysteresis loop to Q' on the air-gap line. Upon reapplication of the demagnetizing force H_d, the magnetization diminishes along the upper branch of the minor loop back to Q. The slope of the line connecting the tips of the minor loop QQ', is a measure of the reversible permeability of the material, which for all practical purposes may be considered constant for loops originating on the demagnetization curve.[4] The magnetization represented by the point Q' is stable to any temporarily applied demagnetizing force not exceeding H_d because the minor loops are reversible. It is evident from Figure 1 that for a given state of magnetization Q' the demagnetizing force for cyclic conditions may be determined from a simple graphical construction.

If the magnet is stabilized by magnetization out of assembly, its magnetization may be represented by M, Figure 2, given by the intersection of the out-of-assembly air-gap line OM with the demagnetization curve. Upon assembly in the magnetic circuit, the magnetization proceeds to M', the intersection of the minor loop with the closed circuit air-gap line. H_d in this case is the maximum demagnetizing force for which the magnet is stable. Its graphical determination is given by the intercept on the H axis of the line MH_d drawn from M parallel to the air-gap line OP.

The two methods described for stabilizing the magnet are fundamentally similar. In the first case, partial demagnetization is obtained by an applied demagnetizing force, while in the second case, partial demagnetization is obtained by the self-demagnetizing force under open circuit conditions.

Under either condition, the operating point of the magnet (B and H co-ordinates) after stabilization is fixed by the terminus of the tip of the minor loop on the closed circuit air-gap line OP. The location of this point is dependent upon the reversible permeability, the degree of demagnetization and upon the slope of the air-gap line OP. The locus of this point, as the air-gap line slope changes from zero to infinity, is a curve passing through H_c and B_r, which will be called a secondary demagnetization curve. This curve may

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P. P. Cioffi is with the Bell Telephone Laboratories Inc., Murray Hill, N. J.

be treated identically as the major demagnetization curve in the magnet design problem. Since the secondary curve lies below the major demagnetization curve, the magnet, under stabilized conditions, will operate at a lower energy level. Thus, the problem is to derive the secondary demagnetization curve from the major curve for given conditions of stability.

The Secondary Demagnetization Curve

A. Stabilization by Magnetization before Assembly

Referring to Figure 2, let M represent the open circuit magnetization having the co-ordinates B_1, H_1, and M' the closed circuit magnetization with co-ordinates B_2, H_2 after assembly in the circuit. OP and OM are respectively the in- and the out-of-assembly air-gap lines. The slope of OM is

$$S_1 = \frac{B_1}{H_1} = \frac{\varphi_1}{A}\frac{l}{H_1 l} = \frac{l}{AR_1} \tag{1}$$

where l is the magnet length, A its sectional area and R_1 is the open circuit reluctance of the air-gap. Similarly

$$S_2 = \frac{B_2}{H_2} = \frac{l}{AR_2} \tag{2}$$

where R_2 is the closed circuit reluctance of the air-gap of the assembly.

From equations 1 and 2 the relationship between S_2 and S_1 is obtained:

$$S_2 = kS_1 \tag{3}$$

where $k = R_1/R_2$. R_1 and R_2 are determined from a magnetic circuit analysis.

The secondary demagnetization curve may now be derived graphically from the major demagnetization curve. Figure 3 shows the major demagnetization curve B_r, P, M, H_c and its corresponding energy-product curve B_r, A, O for the permanent magnet material, Remalloy. Assume a line of slope S_1, and from equation 3 the corresponding line of slope S_2. For this illustration $k = 7.5$. From the intersection M of the air-gap line of slope S_1 with the major demagnetization curve, the line MN is drawn with slope equal to the reversible permeability μ_r. Its intersection with the air-gap line of slope S_2 gives a point M' on the secondary demagnetization curve. Other points are obtained for various assumed slopes S_1, and the curve passing through these points is the secondary demagnetization curve B_r, M', H_c. Its corresponding energy-product curve is the curve B_r, C, D, O. These curves are a function of k alone and families of curves are obtained for various values of this parameter.

The secondary demagnetization curve may also be obtained directly and with higher accuracy by the analytical determination of successive points on this curve as follows:

From Figure 2,

$$\frac{B_2 - B_1}{H_1 - H_2} = \mu_r \tag{4}$$

The simultaneous solution of equations 1, 2, 3, and 4 gives

$$H_2 = \frac{H_1(\mu_r H_1 + B_1)}{\mu_r H_1 + kB_1} \tag{5}$$

and from equation 4

$$B_2 = \mu_r(H_1 - H_2) + B_1 \tag{6}$$

By taking successive values of B_1 and H_1 on the major demagnetization curve, corresponding values of B_2 and H_2 are determined and the secondary demagnetization curve and the energy-product curve may be drawn.

The method of magnetizing the magnet out-of-assembly yields after assembly a degree of stability against demagnetizing magnetomotive forces which may be computed for any point on the secondary demagnetization curve.

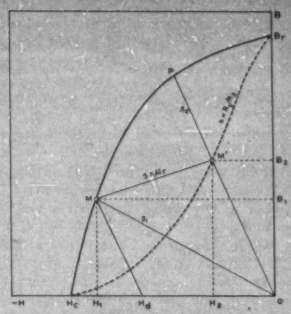


Figure 2. Stabilization by magnetization before assembly

Referring to Figure 2

$$S_2 = \frac{B_1}{H_1 - H_d} \tag{7}$$

the solution of equations 1, 3, and 7 gives

$$H_d = \frac{H_1(k-1)}{k} \tag{8}$$

and the corresponding magnetomotive force to which the magnet is stable is

$$F_d = H_d l \tag{9}$$

where l is the length of the magnet.

For a given stabilizing magnetomotive force, F_d, the point on the secondary demagnetization curve derived before for which the magnet assembly is just stable can be determined as follows. If B_2, H_2 are the co-ordinates of this point, the magnet magnetomotive force is

Figure 3. Secondary demagnetization curve and corresponding energy-product curve of Remalloy for stabilization by magnetization before assembly

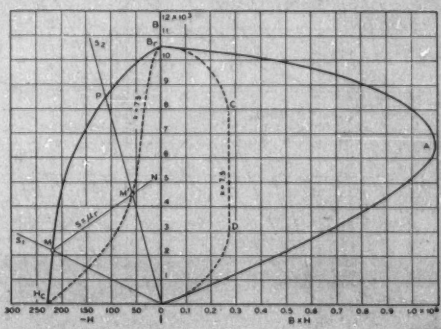
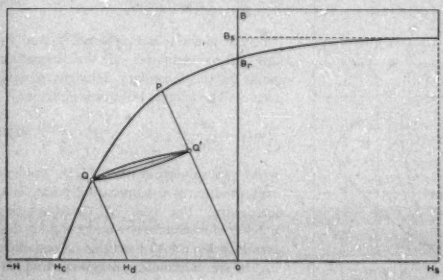


Figure 1. Stabilization by magnetization after assembly



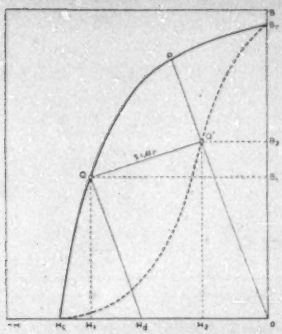


Figure 4. Stabilization by demagnetization with a given magnetomotive force

$$F = H_d l \tag{10}$$

then

$$\frac{F_d}{F} = \frac{H_d}{H_2} \tag{11}$$

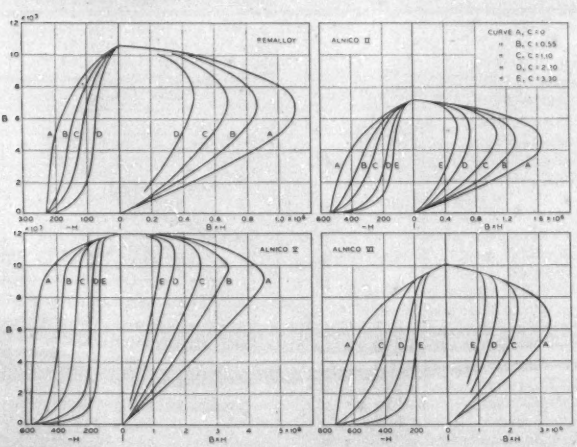
and we will let

$$\frac{F_d}{F} = c \tag{12}$$

that is, the ratio c of the demagnetizing field to the operating field is fixed. From a solution of the preceding equations we find

$$S_2 = \frac{\mu_r[k(c-1)+1]}{k-c-1} \tag{13}$$

Figure 5. Secondary demagnetization and energy-product curves of various materials for constant stability



and the intersection of the air-gap line of slope S_2 with the secondary demagnetization curve previously derived gives the desired operating point of the magnet. The range of the degree of stabilization, c, obtainable is from $\frac{k-1}{k}$ to $k-1$ as S_2 goes from zero to infinity.

B. Stabilization by Demagnetization with a Given Magnetomotive Force After Magnetization in Assembly

In this method of stabilization, the magnet is magnetized after assembly and then stabilized by applying a reverse magnetomotive force, F_d, equal to the maximum magnetomotive force for which the magnet is expected to be stable. The problem is to derive a secondary demagnetization curve for which all points on this curve will provide the same amount of stability. This curve is in general of a different shape from the variable stability curve derived in the preceding section.

Referring to Figure 4, if Q', with coordinates B_2, H_2 represents a point on the secondary demagnetization curve B_r, Q', H_c, being discussed, equations 9, 10, 11, and 12 apply for every point on the curve,

$$F_d = H_d l$$

$$F = H_2 l$$

$$\frac{F_d}{F} = \frac{H_d}{H_2} = c$$

and the secondary demagnetization curve will correspond to a fixed value of the parameter c.

In the graphical solution, the point Q' is uniquely determined by the slope of the parallel lines OP and H_dQ, the reversible permeability μ_r and the ratio c of H_d/H_2. This point may be found by trial on an assumed air-gap line and the curve passing through a series of points determined for various air-gap lines is the required curve.

The secondary demagnetization curve may be obtained directly and with greater accuracy by the analytical determination of successive points on this curve.

Referring to Figure 4,

$$\frac{B_2 - B_1}{H_1 - H_2} = \mu_r \tag{14}$$

$$S_2 = \frac{B_2}{H_2} \tag{15}$$

$$S_2 = \frac{B_1}{H_1 - H_d} \tag{16}$$

The simultaneous solution of equation 11, 12, 14, 15, and 16 gives

$$H_2 = \frac{(B_1 + \mu_r H_1)\left[(c+1) - \sqrt{(c+1)^2 - \frac{4\mu_r c H_1}{B_1 + \mu_r H_1}}\right]}{2\mu_r c} \tag{17}$$

and

$$B_2 = \mu_r(H_1 - H_2) + B_1 \tag{18}$$

directly from equation 14.

By assuming values of B_1 and H_1 on the major curve points on the secondary demagnetization curve corresponding to a given ratio c of F_d/F may be obtained. This curve also goes through B_r and H_c.

Optimum Magnet Dimensions from Secondary Demagnetization Curves

The magnet length, l, in centimeters and area, A, in square centimeters are given by

$$l = \frac{F}{H_2} \tag{19}$$

and

$$A = \frac{F}{RB_2} \tag{20}$$

where R is the reluctance and H_2 and B_2 are the co-ordinates of the operating point on the secondary demagnetization curve. The magnet volume is given by

$$V = lA = \frac{F^2}{RH_2B_2} \tag{21}$$

which is a minimum when H_2 B_2, the energy-product of the operating point, is a maximum. For case (B), therefore, the magnet of minimum size and weight is determined by the proportions corresponding to the maximum energy-product on

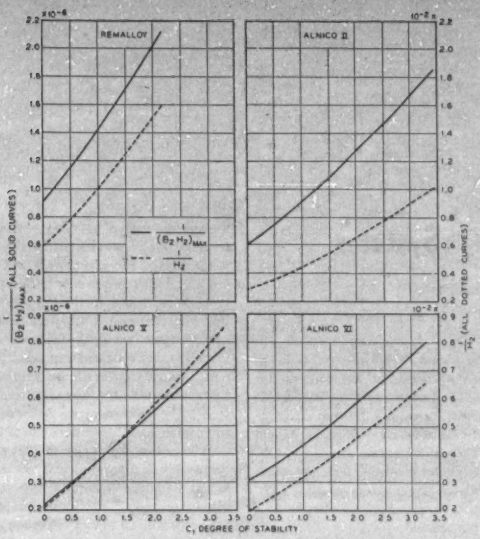


Figure 6. Curves for obtaining optimum magnet dimensions for various degrees of stability

the secondary demagnetization curve.

For case (*A*) under the heading, The Secondary Demagnetization Curve, the minimum magnet dimensions are similarly determined by the maximum energy point on the secondary demagnetization curve. However, if these proportions do not provide sufficient stability, as indicated by equations 11 and 12, it will be necessary to operate at a point on the secondary demagnetization curve given by its intersection with the air-gap line as determined by equation 13. In general when there is a choice between the stabilizing methods of sections (*A*) and (*B*), (*B*) will give the smaller magnet.

Secondary Demagnetization Curves for Several Materials

The secondary demagnetization curves and corresponding energy-product curves, calculated by method (*B*) for several permanent magnet materials for various ratios c of demagnetizing field H_d to the operating field H_2 are illustrated in Figure 5. The curves show that with increasing degree of stabilization, because $(B_2H_2)_{max}$ occurs at slowly increasing values of B_2 and rapidly diminishing values of H_2, the optimum magnet becomes longer and slimmer. The reciprocals of H_2 and (B_2H_2) at maximum energy-product, proportional respectively to the optimum length and volume of the magnet, are plotted as a function of c in Figure 6 and illustrate the rapid increase in optimum magnet dimensions required with increasing degree of stabilization. These curves may be used with equations 19 and 21 for the solution of any problem requiring optimum magnet dimensions over a wide range of stabilization.

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No Discussion

Television Receiving Systems

C. M. SINNETT
NONMEMBER AIEE

THE GROWTH of television during the past year has exceeded the most optimistic postwar predictions. Many factors have contributed, but probably the most important ones were the televising of such events as the Worlds Series baseball games, the Army-Navy football game, the heavyweight fights, and the political conventions which created a public desire for receivers and the development, design, and manufacture of large quantities of popular-priced receiving instruments to satisfy this desire. Thus television has turned the corner and bids fair to become an industry surpassing the automobile industry. Most of the immediate problems have been solved, but new ones are constantly appearing which will test the mettle of the engineering fraternity before complete solutions are found.

Antenna Considerations

Television signals, by virtue of their location in the frequency spectrum, are normally limited to line of sight transmission. Under unusual atmospheric conditions, however, such signals may be received at locations hundreds of miles from the transmitter. At present the first five television channels are located between 50 and 88 megacycles and the remaining seven channels between 174 and 216 megacycles. Each television channel is 6 megacycles wide with the picture and sound radio-frequency (r-f) carriers separated by 4.5 megacycles. Amplitude modulation with one side-band suppressed is employed for picture transmission. Frequency modulation to a maximum deviation of ±25 kilocycles is used for sound. The effective radiated power from television transmitters, for antenna heights greater than 500 feet, is so regulated as to give the same coverage as would be obtained with 50 kw at 500 feet. Thus, if the antenna height is only slightly greater than 500 feet, the effective radiated power can be nearly 50 kw, but must be decreased as the antenna height is increased.

The foregoing information indicates broadly some of the factors which must be considered in the design of a television receiving antenna such as, broad frequency band coverage, controllable directivity, good signal pickup, ease of installation, and low cost. No single antenna, available at present, fully meets all of these requirements. Fortunately, however, with the majority of television transmitting antennas located near the center of a city, it is possible to provide satisfactory reception with comparatively simple receiving antenna structures. These are the half-wave dipole or the half-wave dipole plus reflector used as all channel antennas. Special requirements may dictate the use of a half-wave folded dipole and reflector for the lower frequency channels and a smaller folded dipole and reflector for the higher frequency channels. Under some conditions the foregoing combinations may be necessary to provide satisfactory signal pickup in outlying areas. The dipole and reflector has been found to meet the requirements in a high percentage of the installations to date.

Figure 1 shows the response characteristics of dipoles and reflectors cut for various resonant frequencies. In each case the reflector is spaced approximately 0.2 wave length from the antenna with the antenna directed toward the transmitter. It will be noted that an assembly resonant to the lower frequencies will provide essentially the same signal pickup at the higher frequencies as can be obtained from similar assemblies cut for the latter frequencies. The converse is not true, however, and consequently such antenna systems are tuned to the low frequency television channel. The reflector contributes about 50 per cent additional signal pickup to that obtained from a simple dipole. Folded dipoles while giving greater signal pickup at the resonant frequency tend to be more selective and are used only when local conditions dictate the need for greater signal pickup, then can be obtained from dipole-reflector assemblies. A single folded dipole and reflector assembly providing the increased pickup will serve for the higher television channels, since the total frequency band coverage required of the antenna represents only 20–25 per cent of the carrier frequency employed.

A directivity pattern of a receiving antenna assumes importance when its location is such that reflected signals or man-made static are problems. A high front-to-back ratio, obtained by stacked arrays, may be required under certain circumstances. This is the exception rather than the rule, however, as indicated by the almost universal use of the dipole and reflector assembly.

Figure 2 shows the polar characteristics of the dipole and reflector at 44 mega-

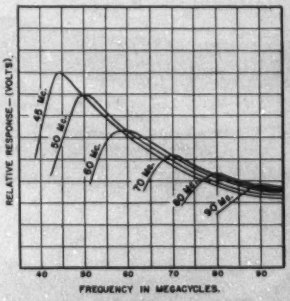

Figure 1. Response of dipoles and reflectors cut for various resonant frequencies

Antennas used with 300-ohm line

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C. M. SINNETT is with the Radio Corporation of America, Camden, N. J.

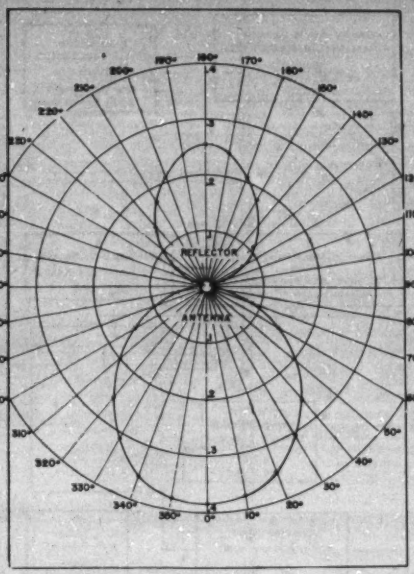

Figure 2. Polar characteristic of dipole and reflector at 44 megacycles

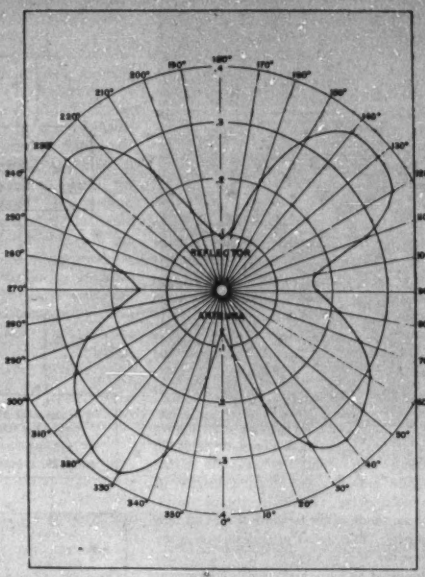

Figure 3. Polar characteristic of dipole and reflector at 174 megacycles

cycles. A front-to-back ratio of nearly two-to-one is obtained with null areas along the axis of the dipole.

Figure 3 shows the polar characteristics of the same antenna assembly at 174 megacycles. The two lobe pattern of Figure 1 has become a sort of four leaf clover pattern with lobes existing at approximately 38 degrees from those at the lower frequencies. Essentially the same pattern is obtained at 216 megacycles. In many instances, this change in pattern between the two television bands works to the advantage of the viewer since a compromise orientation can be made between low and high frequency stations providing optimum performance on both. In other instances two antenna assemblies, together with adequate phasing or switching means, may be necessary.

An antenna which will pick up the signal is only part of the story. On the average the antenna is located some 70 feet from the receiver and requires some form of transmission line between the two. Prior to World War II a transmission line representing the best compromise between cost and performance had losses of the order of 5 decibels per hundred feet at 50 megacycles and 12 decibels per hundred feet at 200 megacycles. Under some conditions, this meant that an increase in antenna elevation could be nullified by the losses of the transmission line extension. During the war a new dielectric, known as polyethylene,[1,2,3,4] was developed for use at ultra-high-frequencies. This dielectric, while expensive, had excellent electrical and mechanical properties. A parallel wire transmission line, using polyethylene as a supporting medium, was developed for use with television antennas and receivers. This line has a characteristic impedance of approximately 300 ohms, is relatively low priced and has extremely low losses. Figure 4 shows these latter characteristics over the

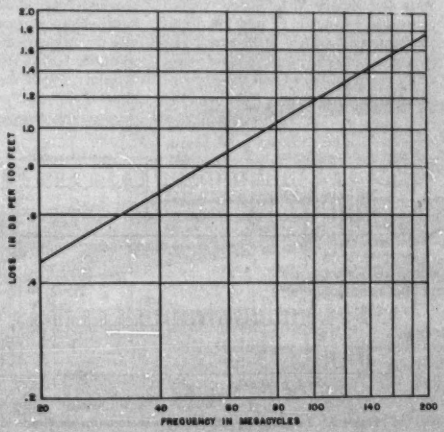

Figure 4. Loss characteristics of RCA bright picture television transmission line

range 20 to 200 megacycles. Even at the highest television frequency, 216 megacycles, the loss is less than 2 decibels per hundred feet. Many other uses have been found for this line such as matching stubs, folded dipole antennas, and impedance transformers.

The Television Signal

The composite television signal contains all the information necessary to synchronize and produce a picture at the receiver. It contains horizontal and vertical synchronizing signals, return beam blanking, and video or picture information including tonal gradations from black to white. The horizontal pulse rate is 15,750 cycles per second and the vertical pulse rate is 60 cycles per second.

Figure 5 shows this composite signal for several horizontal lines and two vertical fields. The television picture employs interlaced scanning 525 lines, 60 fields, 30 frames per second. Observation of the figure indicates the relative heights of synchronizing, blanking, black level, video modulation to white level and zero carrier. This signal, after detection in the receiver, is separated and fed to the video amplifier and the horizontal and vertical synchronizing circuits.

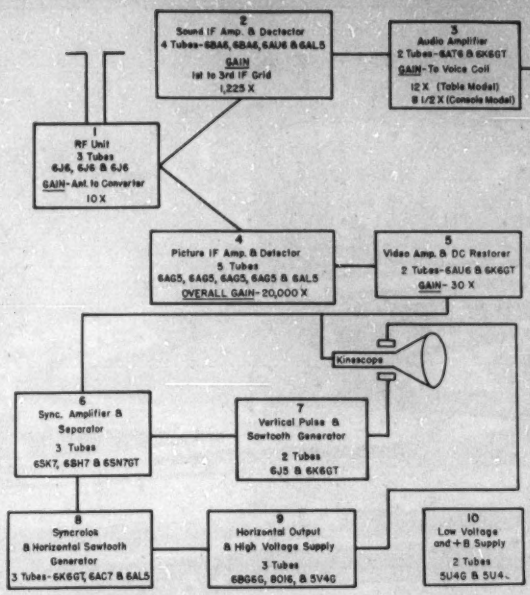

Figure 6. Block diagram of a conventional television receiver

Direct Viewing Receiver

Figure 6 is a block diagram of a conventional television receiver indicating the various functions. The r-f signals, video and sound, picked up by the antenna are fed to the r-f unit. This unit, which can be tuned by suitable means to any one of the 12 television channels, converts the various high-frequency video and sound r-f signals to fixed intermediate frequencies (i-f) of 21.25 megacycles for sound and 25.75 megacycles for video. At the output of the r-f unit these two intermediate frequencies, including the necessary side bands, are fed to the sound and video intermediate frequency amplifiers. The sound channel provides the necessary i-f gain to properly operate conventional frequency modulation (f-m) detector circuits. Audio amplification follows and is fed to a dynamic type loudspeaker. The video r-f amplifier consists, in this case, of stagger-tuned stages to provide the necessary amplification and band width and feeds a conventional amplitude detector. From the detector the video signal is applied to a video amplifier consisting of two stages. This amplifier is a-c coupled and introduces an a-c axis about which the video signal tends to swing unless some form of correction is applied.

This correction takes the form of a

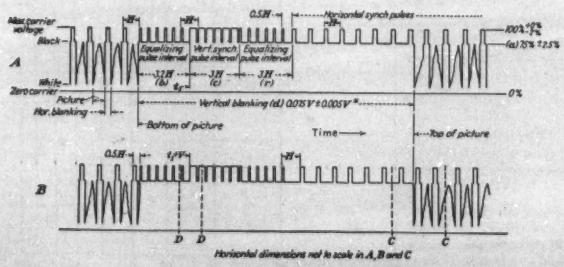

Figure 5. Television synchronizing waveform for amplitude modulation

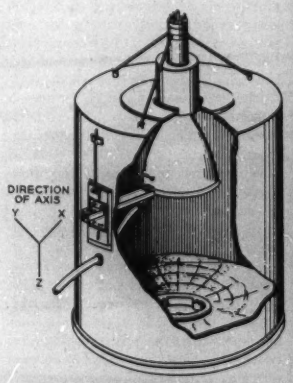

Figure 7. Cut-away view of an optical barrel in the Schmidt optical system

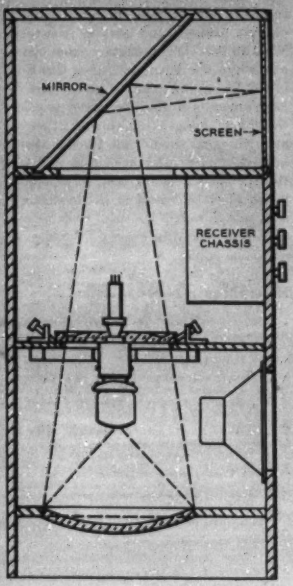

Figure 8. Arrangement of optical system for a home television receiver employing reflective optical principles

This design gives a large-screen picture with a console cabinet no deeper than that of an ordinary home radio receiver

d-c restorer which corrects the picture tube grid bias to suit the various scenes. In addition the d-c restorer circuit provides partially separated synchronizing signals which are applied to the input of the synchronizing signal chain. Here, synchronizing is separated completely from picture and in turn directed to the vertical pulse and sawtooth generator and to the horizontal sawtooth generator and "fly-wheel" circuit. The vertical sawtooth is applied to the vertical deflection coils comprising one portion of the deflection yoke surrounding the neck of the picture tube. The fly-wheel circuit in addition to providing a pulse for proper operation of the horizontal discharge tube, insures complete horizontal synchronization of the picture under practically all conditions met in the field. The horizontal discharge or output tube provides the necessary deflection current to the horizontal windings of the deflection yoke and in addition, through a step-up transformer provides approximately 9,000 volts, after rectification, to the picture tube. A conventional low voltage power supply completes the receiver.

For all its apparent complexity a television receiver is comparatively easy to operate. Seven major controls are provided on the front panel comprising station selection, fine tuning for sound, horizontal synchronizing, vertical synchronizing, picture contrast, picture background, and an audio volume control. As the art progresses it may become feasible to eliminate front panel controls of horizontal and vertical synchronizing, fine tuning and picture contrast. At the present time several other screw driver adjustments are provided on the chassis for horizontal size, vertical size, horizontal and vertical centering, linearity, and focus. When properly adjusted at the time the receiver is installed readjustment is seldom required.

Projection Receivers

While the mass market for television receivers will probably involve the lower-priced table models, there is a relatively large demand for pictures of larger size than can be produced economically by means of direct viewing picture tubes. To meet this demand, various projection systems have been proposed. Two of these have assumed some prominence; the refractive or conventional lens system and the reflective or Schmidt system. Neither of these fully satisfies the requirements. The refractive system, while more flexible in adapting picture size to varying viewing conditions and requiring less cabinet space, is not as efficient as the Schmidt system. Since light output is expensive to obtain in a television receiver, the trend has been toward the more efficient Schmidt system.

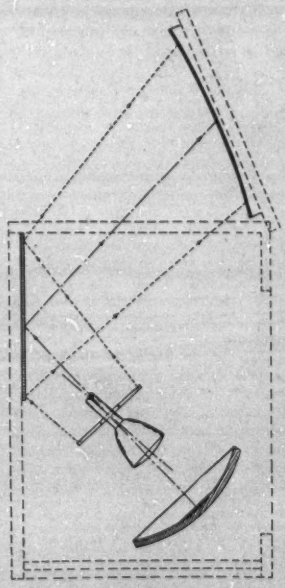

Figure 9. The Schmidt optical system using a reflective screen

Figure 10. The Schmidt optical system using a 42-inch spherical mirror and a 12- or 15-inch picture tube

Figure 7 shows a cut-away view of an optical barrel such as is used in the Schmidt optical system. Normally, a four- or five-inch diameter picture tube, operating at 27,000 volts second anode, is used. The surface of the face of the picture tube has a curvature which is designed to work properly with the spherical mirror and aspherical lens of the reflective optical system. The tube faces the mirror which is located in the bottom of the optical barrel. Reflected light rays from the spherical mirror pass through an aspherical lens and are brought to sharp focus on the viewing screen. By proper design it is possible to construct such an optical system with a magnification of six, a "throw" of roughly 36 inches and obtain resolution of over 1,500 lines in the corners of the picture. The effective aperture of such a system is of the order of f 0.8.

For use in the home, it is desirable that

the viewing screen, used for television projection, possess certain optical characteristics. A picture will seldom, if ever, be viewed from the extreme sides because of image distortion; consequently, the screen is provided with directional characteristics which conserve the available light and result in a gain in light output over that obtained with the ordinary diffusing screen. Experience has indicated that such a screen, possessing a horizontal included angle of roughly 60 degrees and a vertical included angle of 15–20 degrees, will meet practically all the requirements. In a transmissive screen the horizontal and vertical directivities are obtained by means of vertical ribs on the front surface of the screen and a Fresnel lens on the back, the screen having been moulded from clear plastic with a diffusing interlayer. In a reflective screen a metal reflector is provided with microscopic vertical lines plus a light spray of diffusing material to obtain horizontal directivity and is then slightly curved about a horizontal axis to provide the desired vertical directivity. With either screen a gain in light output of the order of five to seven times that of the ordinary diffusing screen is obtained. Both systems are in current use and provide a picture of adequate brightness and resolution having dimensions of 20 inches by 15 inches.

Figure 8 shows the general layout of the Schmidt system using a transmissive screen.

Figure 9 shows the same type optical system employing a reflective screen.

The Schmidt optical system has been employed for theatre television projection on a screen 24 feet by 18 feet. Figure 10 shows such a system using a spherical mirror 42 inches in diameter and a picture tube which may be either 12 or 15 inches in diameter. This constitutes the largest Schmidt-type optical projection system in use in the world today.

In conclusion it should be stated that the foregoing discussion of television receivers is of necessity very brief. Many items of interest and importance have been omitted for the sake of brevity. It is sufficient to say that television is undergoing rapid changes which will eventually result in lower priced receivers having clearer pictures and greater contrast. With all that is being learned about black and white television and the application of this knowledge, plus the expenditure of large amounts of time and money to color television, its eventual introduction is assured. One should not be impatient, however, since color television will introduce many new and difficult problems which must be solved in the laboratory and substantiated by field tests before consideration can be given to it as a service to the public.

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No Discussion

The Characteristics and Application of Precision, Snap-Acting Switches

A. L. RICHE
MEMBER AIEE

FROM the beginning of the use of electric power, switches have been required for its control, but until recently attention has been given to reducing their size, weight, and operating energy requirement, except for the work done with mercury contacts. Growth of the use of automatic controls, where mechanical motion is required to open and close electric circuits, has brought demand for units of greater refinement than those typified by the ordinary toggle switch and for freedom from the limitations of tilting mercury tubes.

The first of the family of devices now known as precision, snap-acting switches, made its appearance in 1927, and by 1932, variations of the original device gained commercial acceptance as the contacting elements in thermostats which controlled one-half horsepower oil burner motors, at line voltage, without the use of intermediate relays.

The reliable performance of these switches in thermostats led engineers to try them for other purposes, and it was soon recognized that here was a new tool of versatile usefulness. Growth in the variety and extent of its application has been so rapid that engineers and designers are not completely informed on many phases of the usefulness and the limitations of this newcomer among electrical controls.

The purpose of this paper is to outline the characteristics and the points which must be considered in the use of precision, snap-acting switches.

Dimensions, Operating Principle, and Characteristics

The dimensions and operating principle of the switch which was introduced in 1932 are indicated in Figure 1, and the characteristics of a typical unit, as now manufactured, are listed below.

1. Small size ($1^1/_4$ cubic inches).
2. Light weight (0.8 ounce).
3. Life of millions of operations when used on small loads.
4. Precise repeatability of operation at a given point in mechanical motion of the actuating plunger, maintained for many thousands of operations.
5. Operability by small force (10 ounces).
6. Operability by small motion (0.001 inch).
7. Resistance to the effect of vibration.
8. Capacity to control loads of 20 amperes, at potentials up to 460 volts alternating current.
9. Operability with substantial uniformity when tipped into different attitudes (as distinguished from the performance of mercury switches).

The basic operating principles shown in Figure 1 are used in many variations in size of unit and in operating means, which have been built to meet particular needs. Figure 2 illustrates a few examples.

There are now several manufacturers offering units which fall into the class of precision, snap-acting switches, some of which are interchangeable in size with those shown in Figure 2. A variety of operating principles differing from those shown in Figure 1 are used, but all employ noble metal, butt contacts driven by blade-type snap-acting mechanisms. Spring members usually are made from heat-treated beryllium copper. Most types are self-returning, but maintained contact varieties are available.

In addition to the variations in size, modes of actuation, and operating mechanisms, the characteristics of each type of switch are varied to meet the needs of particular applications. The terminology which has been developed for convenience in describing these characteristics is shown in Figure 3.

Selecting the Switch Best Suited to Each Job

The need for care in selecting switches for each application arises from the great variety of styles and operating characteristics which have been produced to meet the range of use-requirements. Two examples will illustrate the breadth of the range.

Among the 300 switches used in the 200-inch Mount Palomar telescope,[1] some operate only in emergencies but must repeat operation within 0.0002 inch of the

Figure 1. Basic switch operating mechanism

The long spring member is mounted in cantilever. Two shorter members rest in V's, which are positioned to bow them in compression. The contact, mounted on the free end of the spring, is held against an upper, fixed contact by the couple action of tension and compression forces. When plunger motion deflects the tension member downward, reversal of couple snaps the contact downward. Upon removal of operating force, cantilever action restores the system to its initial position, with snap motion

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A. L. Riche is vice president of the Micro Switch, division of First Industrial Corporation, Freeport, Ill.

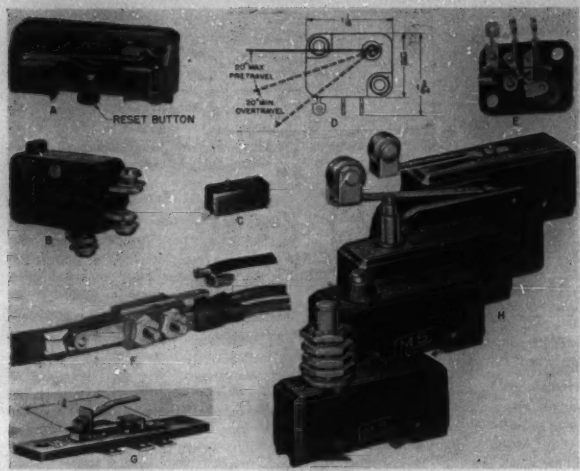


Figure 2. Examples of typical varieties of switch sizes and actuating arrangements

A. Maintained contact type
B. Compact design
C. Subminiature size
D. Rotary actuating motion
E. Mechanism of rotary actuated design
F. Open blade unit with separable, solderless connector
G. Skeleton type
H. Assorted built-in overtravel mechanisms

same point of actuating motion, regardless of changes in ambient temperature or lapse of time between operations. On the telescope, switches are not subject to vibration or impact and the maximum electric load on any switch is a 1/50th horsepower motor. Contrasted with this are switches of the same general appearance, but having different characteristics, which are used on giant punch presses and in rail sanding devices on railroad locomotives. In both of these uses impact and vibration are severe, reliability over many thousands of operations is the prime requirement, and in some cases heavy electric loads must be controlled, but the highest degree of repeatability is not needed.

Repeatability

The gauging operations and control of machine tool motion, shown in Figure 4, illustrate the use of switches where repeatability must be within ±0.0001 inch in 100,000 successive operations, and must not be measurably affected by normal changes in ambient temperature. When repeatability of this order is required, the operating plunger is carried in a close-fitting guide, and other precautions are taken to insure uniformly maintained point of application of operating force to the snap-acting spring.

Where high repeatability is required, electrical loading of contacts must be no greater than 10 watts at 120 volts alternating current or 5 watts at 25 volts direct current. The reason for this is seen in Figure 5, as even moderate electrical loading of contacts causes sticking which must be overcome by operating motion greater than that required to cause mechanical switch actuation at no load.

Operating Force

The total force required to operate a switch varies from 3 to 28 ounces, depending on the thickness of spring material selected for a given design, and with contact separation. Total operating force is often of less interest than differential force which, as shown in Figure 3, is the difference between total operating force and release force.

Small Differential Force

Switches operable by differential force as low as $1^1/_2$ ounces are used in sensitive thermostats of the type shown in Figure 6, in which partially-filled wafers or bellows are used as the temperature-sensitive element. Such elements provide ample motion, but the available differential force is small. Figure 7 illustrates the use of a force-sensitive switch for counting drops of dew as they are deposited on the end of a long lever arm.

Small Differential Motion

The expansion and contraction of metals or solid-filled bellows, such as are frequently used as the temperature sensitive element in thermostats, provide ample force, but the motion available for contact operation is extremely small. Switches designed for use in such instruments operate with a differential motion of no more than 0.0002 inch. This same type of switch is used to detect the deformation of machine parts beyond predetermined limits.

Switch Sensitivity Is not Synonomous with Precise Repeatability

Repeatability refers to the precision with which a switch operates in relation to a given point in the motion of the operat-

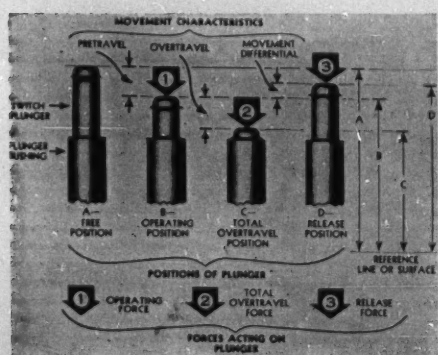


Figure 3. Definitions of special terms

Differential force = operating force minus release force

Repeatability is stated in terms of decimal fractions of an inch and refers to the maximum variation from a fixed point at which motion of the actuator of a switch causes it to snap over. It is usually specified with reference to a particular number of switch operations, such as 100,000

ing plunger, and has no relation to the differential motion, differential force, or the product of the two (which is differential energy) required to actuate that switch. Misunderstanding on this point has often led to the specification of switches having unnecessarily close differentials and consequently lower electrical capacity or shorter contact life than can be secured from switches of wider differential characteristics. Precision of repeatability is accomplished by construction which maintains a fixed point of application of operating force to the snap-acting switch blade.

Performance of Sensitive Switches on Large and Small Loads

Switches operable on small loads by mechanical energy on the order of 0.0008 ounce-inch are capable of opening and closing circuit on loads of several amperes at potentials up to 460 volts alternating current, but on such loads operating energy is increased by widening of both differential force and motion, as shown by Figure 5.

Special contact problems arise when such sensitive switches are used to control loads on the order of milliamperes or millivolts, for not only is initial pressure between contacts limited, but in all switches of this type contact pressure approaches and passes through zero at the instant of snapover. Experience shows that smooth, clean contact surfaces of the purest silver will not close circuit reliably on milliwatt loads. Good performance on such loads is accomplished by use of a serrated contact surface mating with a smooth-surfaced contact. One form of such serration is shown in Figure 1. This is further discussed under Contact Materials and Conformation.

Resistance to Acceleration, Impact, and Vibration

High pressure between contacts is necessary, not only for high electrical capacity but to resist the tendency of motion of the entire assembly to jar them apart or into false contact position. Contact pressure can be increased by the use of stiff spring elements, but it also bears the relation to contact separation as seen in Table I in terms of resistance to acceleration.

Switch Life

Switch life, referred to in Table I as probable mechanical life, represents the medium value of the number of operations performed before failure, as determined by a large number of tests.

On small electric loads, life is limited by the mechanical life of the snap-acting mechanism. On heavy electric loads, switch life is limited by disintegration of the contact material or the roughening of contact surfaces to the point where there is danger of sticking due to mechanical interlocking of the roughened surfaces. As indicated in Table I, mechanical life varies directly with contact spacings as a result of the necessary spring flexure. Contact life varies so widely with the type of switch and nature of the electric load that no comprehensive tabulation can be set down here. In many instances, however, probable life of 1,000,000 operations or more will be experienced when switches are loaded to their rated capacity.

Switching Capacity and Underwriters' Laboratories Rating

Precision, snap-acting switches are listed by Underwriters' Laboratories under the Special Use Snap Switch Standard.[2] Test procedures and listings under this standard provide only ampere, voltage, and sometimes horsepower ratings. These data are obviously insufficient for use by designers in determining the safety of switches on many applications, for they are silent as to the effect of power factor and the inrush current which must be handled on solenoid loads. Horsepower ratings are almost meaningless because of the irregular nature of motor characteristics.

Table I

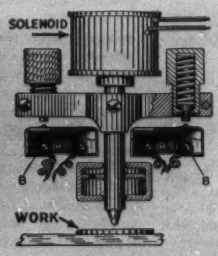
Switch Type	Contact Separation, Inches, Nominal	Resists Acceleration, Stated in G*, Up to	Probable Mechanical Life, Operations
Highly sensitive	0.008	35 to 45	45,000,000
General purpose	0.020	75 to 80	20,000,000
Special wide contact separation	0.070	150 to 200	500,000

* G equals 32 feet per second per second.

A special section of the National Electrical Manufacturer's Association (NEMA) is now writing a standard which is intended to make ratings of precision, snap-acting switches meaningful and a reliable guide to their safe application. It must be remembered, however, that such a standard is a guide only to maximum safe loadings and that there are many uses for precision, snap-acting switches where heavy loading is not compatible with precision of repeatability nor with maximum contact life. On points of this kind, NEMA standards and Underwriters' listings are of little value, but switch manufacturers have accumulated much data which are gradually being organized and published. This is, however, a relatively new field and there are still many questions which can be answered only by special tests.

Paralleling preparation of an NEMA standard, Underwriters' Laboratories has undertaken special tests to determine the safety of certain switches under conditions of operation not covered by any standard and will apply the results of such tests in judging other applications. One such series of tests has been applied to a plastic-enclosed switch, of the size shown in Figure 1 but of different construction,

Figure 4. In and out motion of the cutting head on a machine tool is controlled by the switches shown at A



Pieces are gauged to close tolerance by the switches shown at B. Work above-tolerance in thickness stops downward ram motion before either switch is operated. Actuation of one switch only indicates within-tolerance and a dimension below-tolerance causes operation of both switches. An automatic version of this device is used for sorting optical blanks into three categories

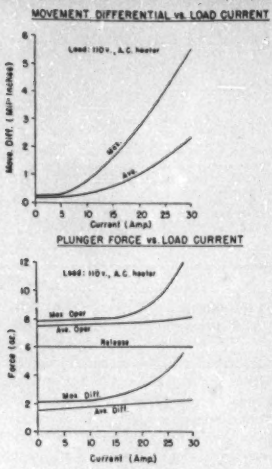
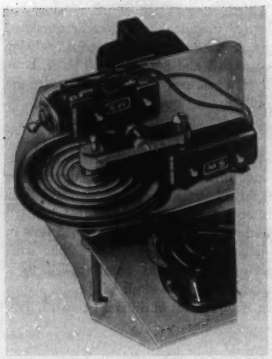


Figure 5. Force and movement differential characteristics



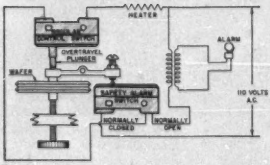


Figure 6. Sensitive thermostat with safety alarm

The wafer operates directly against the main switch but if heat is not cut off by the regular switch, continued expansion moves the lever to operate a safety switch which both takes over control and closes the alarm circuit

to determine the suitability of the following rating.

Inductive Load
- Inrush 75 amperes, 460 volts, alternating current
- Steady State 20 amperes, 460 volts, alternating current

Heater Load 20 amperes, 460 volts, alternating current

Motor Load 3/4 horsepower 120 volts alternating current, $1^1/_2$ horsepower 240 volts, alternating current

Lamp Load 10 amperes, 125 volts, alternating current

Maximum permissible continuous ambient temperature 250 degrees Fahrenheit

A rating of this type is based on the experience that interruption of an a-c load of any kind is not the limiting factor, for within the voltage rating there is never any difficulty with extinguishing the arc. For example, on tests to establish the above rating, full inrush current of 75 amperes at 460 volts and at 35 per cent power factor was repeatedly interrupted with no arc persistence. This is distinguished from the effect of such loading on contact life.

Underwriters' Snap Switch Standard specifies a temperature rise of 30 degrees centrigrade above normal ambient as the rating limitation. This has been shown to be unnecessarily low when silver contacts are used[3] and spring elements are of beryllium copper or other material which retains its characteristics at high temperature.

The permissible continuous ambient temperature rating shown previously is determined by checking switch performance over 100,000 operations, under full electric load, during a 90-day period, while exposed to the rated ambient temperature.

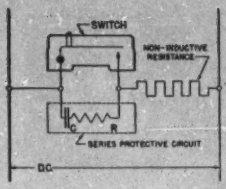
The factors limiting switch ratings are: welding together of contacts on heater loads, and contact life on inductive loads. The reason for this limitation on heater load rating is that, unless special precaution is taken to prevent it, contacts come together with a bouncing motion, and the resulting arc melts contact materials which cause a certain degree of welding together of contact surfaces. On a heater load full current flows in the arc from the very beginning of the first bounce, whereas on inductive loads, such as motors or solenoids, full current does not build up until after the period of bounce is passed. The time during which high inrush persists on such loads is so short that inrush current many times the value of permissible steady state current can be handled satisfactorily. The same is true of a tungsten lamp load, for a lamp which draws 10 amperes after equilibrium is reached permits an instantaneous inrush on the order of 125 amperes when the filament is cold.

Contact Materials and Conformation

Fine silver contacts satisfactorily serve most purposes. However, smooth silver contacts, even though perfectly clean, cannot be depended upon for reliable closure of circuits on either a-c or d-c loads of the order of millivolts or milliamperes. Satisfactory performance has, however, been accomplished in switching thermocouple circuits and on minute a-c loads in aircraft circuits, by use of a fine silver serrated contact working against a smooth silver surface. Such a surface is shown in Figure 1. This success is attributed to increased unit pressure, as compared with that existing between smooth contact surfaces, the establishment of multiple contact points, and reduction of the probability of minute specks of foreign material holding contact surfaces separated. It is also probable that the knife-like edges of serrations cut through surface film on the smooth mating surface as the two come together with wiping motion. Tests of the performance of this contact arrangement have included introduction of fine dust of insulating material which causes immediate failure of smooth contact surfaces. Exposure to humidity and an atmosphere of sulphur dioxide has shown marked improvement in performance of serrated contacts, as compared with smooth-surfaced contacts. The good showing of the serrated surfaces under these tests is supported by many years of satisfactory field service under conditions where smooth contacts had given a great deal of trouble previously.

Figure 7. Drops of dew fall onto switch actuating arm and are recorded on electric counter

Since it is desirable to use one form of contact for the widest practical range of applications, field and laboratory testing has been undertaken to determine the suitability of the serrated contact for use



Protection for D.C. Heater and Incandescent Lamp Loads				
Load in Watts	115 Volts DC.		230 Volts D.C.	
	C (mfd)	R (ohms)	C (mfd)	R (ohms)
0 to 60	0	0	0	0
61 to 110	$\frac{1}{4}$	0	$\frac{1}{4}$	2
111 to 230	$\frac{1}{2}$	$\frac{1}{2}$		
231 to 500	1	$\frac{3}{4}$	$\frac{1}{2}$	1
501 to 750	2	1	1	1
751 to 1000	3	$\frac{1}{2}$	2	2

Figure 8. Contact protection on d-c loads

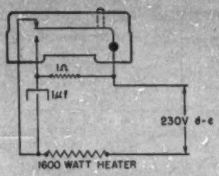


Figure 9. Special contact protection for large d-c heater loads

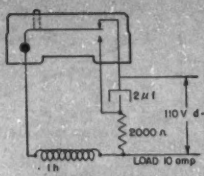


Figure 10. Special contact protection for large d-c inductive loads

on heavy, as well as extremely small, loads. The results of these tests have shown serrated contacts to be generally useful.

Contact Materials Other Than Silver

Pure silver is not the ideal contact material when electrical loading is such as to produce splash, which so contaminates insulating surfaces as to invite flashover. On d-c loads silver tends to transfer from one contact to the mating surface in the form of a peak and crater, and on all loads above a few watts there is some degree of sticking. All of these deficiencies stem from the low melting point of silver, and most attempts to remedy them are directed at so alloying silver as to retain its desirable features of high electrical and thermal conductivity and resistance to the formation of nonconducting surface film, and at the same time raise its melting point. Among the many proprietary silver alloys available, those containing copper, nickel, iron, and cadmium oxide have been found most useful in precision, snap-acting switches. There is not yet enough experience to justify general recommendations, but there is reason to believe that improved contact performance on some specified loads can be accomplished by selection of a contact alloy suitable to each.

Use on D-C Loads

This type of switch was designed primarily for use on alternating current, but d-c applications constantly arise. Millions of such switches have been used on 30-volt d-c aircraft applications on loads up to 25 amperes. Most switches for such use have contact separation of 0.070 inch, which separation is necessary to insure circuit interruption without sustained arcing, particularly at high altitudes. In general, no special arc-snuffing circuits are used on such low-voltage installations. On heavier aircraft loads, particularly where the frequency of operation is high, a special form of switch is used in which the arc is blown out by a permanent magnet.

Noninductive Loads

On d-c heater loads up to 60 watts at 115 and 230 volts, no contact protection is required. The circuit and values of capacitors and resistors to be used above that loading are shown in Figure 8. Use of this circuit not only insures snuffing of the arc but minimizes the transfer of contact material, which is always a serious problem on d-c loads. On loads above seven amperes at 230 volts, the circuit shown in Figure 9 is often effective. It has the particular advantage of being noncritical.

Inductive Loads

The switching of d-c loads, where even a moderate amount of inductance is present, introduces problems, many of which can be solved only by cut and try testing. There is no means for accurate, advance calculations of the performance of such circuits, and the usual procedure is to try various combinations of capacitors and resistors, connected in the circuit shown in Figure 8, and observe the character of the arc formed between switch contacts. Experience develops ability to judge when the best combination has been found, but in many instances extensive tests are necessary to supplement that judgment. In many instances the circuit shown in Figure 10 is found more useful than others, particularly on loads above those set out in the table of Figure 8. The values of capacity and resistance, shown in Figure 10, have been successfully used on the load there indicated, but, as mentioned in connection with the circuit shown in Figure 9, these values are not critical as are those shown in Figure 8 and may be used on loads varying materially from those indicated. The theory of the operation of this circuit is not well understood, but experience has shown it to be effective as an arc suppressor. It does not, however, inhibit the transfer of contact material as well as the simple combination of capacity and resistor connected across the switch contacts.

Double Pole, Double Throw Switches

Most precision, snap-acting switches are of the single pole variety. There has, however, been an ever-increasing demand for double pole construction, particularly for use on aircraft and by the Signal Corps. The types developed during the war have not been entirely acceptable because of their size and weight. This is shown full scale in Figure 11, together with a new unit which is interchangeable in size with the commonly-used single pole switch. See Figure 1 for dimensions. This new unit provides for simultaneous action of both poles in each direction, with sustained positive pressure between contacts up to the instant of their

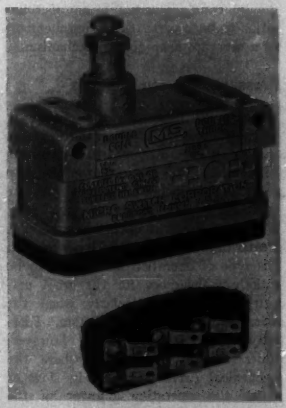
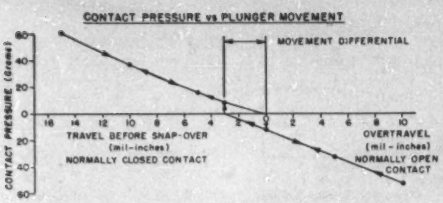
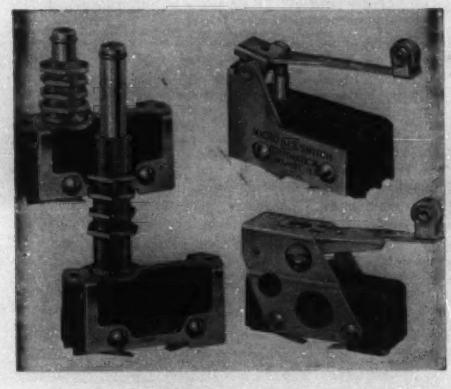


Figure 11. Double-pole double-throw switches

The smaller switch is physically interchangeable with switches of the size shown in Figure 1





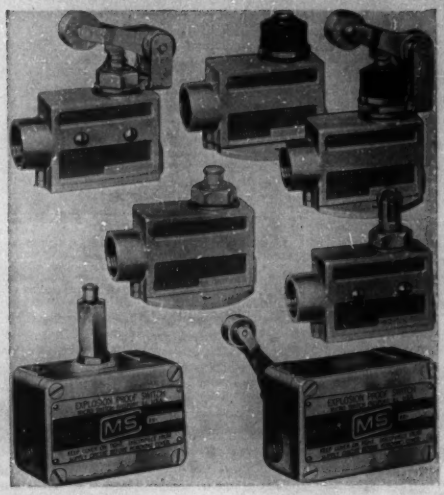


Figure 14 (right). Protective housings and overtravel actuators

Figure 12 (above). Contact pressure characteristics

Figure 13 (below). Bracket overtravel actuators

separation. This is accomplished by simultaneous application of hammerblow action to both current-carrying members by a separate snap-acting mechanism.

Pressure Between Contacts

Figure 12 illustrates the change in pressure between contacts as the operating plunger moves through the operating differential. This shows that as the snap point is approached contact pressure approaches and passes through zero. When the speed of operating motion is greater than about 0.001 inch per minute, this type of operation has proved to be entirely satisfactory for the great majority of uses, particularly when the switch is not subject to vibration. Difficulty may be experienced, however, under certain conditions, where the mechanical drive is extremely slow or where the operating plunger of the switch normally is held near the operating point most of the time, as in thermostats. The difficulty is exaggerated if the switch is so mounted as to be vibrated synchronously with the power supply, as, for example, by the vibration of a motor or by air pulses from a fan. In such an assembly the switch is moved bodily in one direction on one half of the cycle and in the other direction on the second half. Inertia of the moving contact tends to hold it stationary which results in the switch acting as a rectifier. Under such conditions the contacts rapidly deteriorate due to arcing on the half-wave rectified current. When it is not practical to avoid such synchronous vibration, switch contacts can be so mounted as to sustain positive contact pressure up to the instant of separation.

Auxiliary Actuators and Housings

For many uses the mechanical motion available for operating switches is either so wide or so lacking in uniformity as to make its direct application to precision, snap-acting mechanisms impractical. Overtravel devices must necessarily be introduced to absorb the excess or variable operating motion. Some of the mechanisms available as parts of basic switches are shown in Figure 2. Others are made portions of special brackets, or of the metallic housings which are required for protection of basic switches against mechanical damage, oil, coolant, water, or corrosive or explosive atmospheres, Figures 13 and 14.

Future Development of Switches

The field of usefulness of this general type of switch will expand with ever-growing use of automatic and semiautomatic mechanization of manufacturing, merchandising, the control of temperature, humidity, and such functions as the lowering and raising of windows. These will be met by refinements and adaptations of present switches, with the probability that there will become available completely sealed, compact, lightweight units free from all environmental effects and filled with an inert gas under such pressure as will adapt it to direct as well as alternating current use, in a manner not attainable with present designs.

References

1. Electrical Features of the 200-Inch Telescope, Bruce H. Rule. Electrical Engineering, volume 61, February 1942, pages 67–78.

2. Special Use Snap Switch Standard. Underwriters Laboratories (Chicago, Ill.), August 1941.

3. Suggested Changes in Temperature Limits of Present Standards for Stationary Contact Surfaces. Electrical Engineering, volume 67, March 1948, pages 253–57.

Discussion

L. M. Perkins (Allen-Bradley Company, Milwaukee, Wis.): The development of precision snap-acting switches in the multitude of forms and the accuracy of performance in which they are now available is an impor-

tant contribution to industry. I am sure we all appreciate having had the opportunity of reading Riche's clear and interesting discussion of the many and varied applications to which these switches are adapted, and he and his associates are to be congratulated for their important contributions.

Riche has discussed one of the more common types of switches and has described the wide field in which it can be expected to give excellent performance. He has also brought it to our attention that there is a limited field in which the same excellent results cannot be expected with this construction of switch. This limited field includes temperature controls, pressure switches or mechanical motions where the rate of change is so slow that the switch may be held in one position near the snap-over point for an appreciable time.

Of the other types of precision snap-acting switches available, some give their best performance in the same field as the switch discussed in the paper. Others are available for use in those particular applications having slow application of operating force where th switches discussed do not give their best performance. These different types are similar in having the contact mounted at the end of a flexible spring but they differ in the form of the toggle spring used to produce snap over and also in the place where the operating force is applied.

As you know, the switches described by the author consist basically of a contact mounted near the free end of a flexible member with the two integral edge strips so bent down and so pivoted as to give the effect of a compression toggle spring pivoted at a point below the axis of the flexible member. This construction holds the moving contact against a stationary contact mounted above the axis. The operating force is applied to the central portion of the flexible member near its fixed end. As this force is increased, it acts to decrease the contact force both because of the beam action of the flexible member and because the line of action of the tensile force in the flexible member is brought closer to the stationary pivot of the toggle. When the contact force reaches and passes through zero the contact will snap across from the upper to the lower stationary contact. It is possible by change in construction details to increase or decrease the force between contacts immediately after snap over but the contact force must always pass through zero before snap over can occur. The smaller the force between contacts after snap over the less will be both force and travel differentials but also the less will be the current handling capacity of the switch.

In another of the more common types of these switches, the contact is likewise mounted at the free end of a flexible member but the toggle spring is pivoted at the end of a second spring instead of on a rigid point. The operating force is applied to this second cantilever spring near its fixed end, instead of directly to the contact spring as in the principal type discussed in the paper. This second type can be so proportioned as to fit the same applications as the type first discussed, in which case the contact force will go through zero before snap over just as in the first type. However, it is also possible to so modify this second construction that the contact force will not drop to zero except at the instant of snap over when the moving contact is leaving one stationary contact to

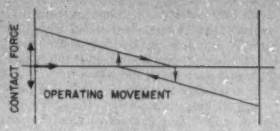


Figure 1

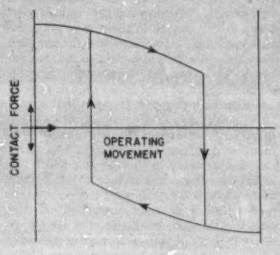


Figure 2

travel over to the other stationary contact.

To obtain this result it is necessary to add resilience between the operating force and the end of the second cantilever spring where the toggle spring is pivoted. This may either be a spring, interposed between the operating force and the cantilever spring, or the cantilever spring itself may be made more flexible between the point where the operating force is applied and the end where the toggle is pivoted. For correct operation this resilience must very closely balance the toggle action, with the toggle a little stronger than enough to reach this balanced condition.

This construction gives the results shown in Figure 2 of the discussion where increase in operating movement causes the contact force to decrease to a certain point and then to suddenly pass through zero to a higher value against the other stationary contact. One important feature of this construction is that the switch cannot be so operated as to hold a condition of almost zero contact force where slight vibration would cause arcing at the contacts. Another feature is that the change in force at the contact on snap over is so large that incipient welds are broken loose without any effect on the normal differential of the switch such as is shown in Figure 5 of the paper. A third feature is that the higher contact force after snap over increases the ability of the switch to close on and carry larger currents under conditions of slow change in operating force combined with vibration.

So this modified type, including resilience between the operating point and the toggle, is a switch which can be used with excellent results on those applications where the switches may not give satisfactory performance. It is obvious that this increase in contact force before snap over can only be obtained at the expense of greater differential in both force and travel.

Royce E. Johnson (Barber-Colman Company, Rockford, Ill.): Riche has included a large amount of useful information in his paper, for which I wish to compliment him. I am certain that had it not been for the pioneering work by the author and his associates in developing the precision snap-acting switch, the state of the art would be much less satisfactory than it now is. Lack of such switches would have greatly handicapped our industrial, military, and aircraft control development in World War II.

It is interesting to translate Riche's figures for switch operating power and contact performance into the concept of power amplification. The curves in Figure 5 indicate that a differential force of about 2 ounces and maximum movement of 0.0008 inch will handle a 1,100-watt resistive load. Assuming that this net input of 0.0016 inch-ounces takes place in 0.1 second, the power represented is 0.00013 watts. The ratio of 1,100 to 0.00013, or 8,500,000, then represents the power amplification of this device if computed for a 0.1 second time interval. For a 1-second interval the ratio would be ten times greater. The development of pulse techniques for use in electromechanical servo systems may provide another application for this switch which is, in effect a power amplifier.

The travel time of the armature in opening one contact and closing the other in a type *BZ* switch is between 5 and 6 milliseconds, a period which is appreciably shorter than the operating time for ordinary relays. This too is a desirable characteristic in servo controls.

Results of life tests which we have run, using alternating current, are in agreement with Riche's statement that a heater load is more difficult to handle than the same current for an inductive load. Oscillograms show that there may be as many as three bounces when the contacts close, which subjects the contacts to several opening and closing operations at a rate of between 1,000 and 2,000 times per second. The full current is handled at each operation, as the author states, when the load is noninductive.

This bouncing of the contacts leads to the subject of antibounce contacts. Would it not be possible to mount the moving contact on a secondary slightly preloaded spring, or to cushion mount the stationary contacts so as to absorb the energy which causes contact bounce? Such construction might be warranted for certain applications requiring long life or freedom from bounce for other reasons, one of which is radio noise.

An arc suppression circuit which we have found even more effective than the one shown in Figure 10 for a 10-ampere 110-volt resistive or inductive load uses a 2-microfarad capacitor and about 1/4-ohm resistance connected across the contacts as in Figure 8, but includes in addition a small half-wave selenium rectifier in parallel with the load. The polarity of the rectifier is such as to prevent passage of current when the switch is closed. This virtually short circuits the inductive transient voltage of the load when the contacts open. A rectifier rated at only 0.1-ampere 110-volts continuous duty lasts quite well where contact operations occur 500 times per minute. This arc suppression circuit, which we use (with different constants) for sensitive polarized relays, keeps the switch cool whereas it gets quite warm within a few minutes if the circuit of Figure 10 is used. Oscilloscopic observation shows that the rectifier circuit prevents contact arcing while for Figure 10 the

arcing persists until a few milliseconds after the opposite contact has been made.

A. L. Riche: Perkins' discussion expands matter referred to only indirectly in the paper, and on which I am glad to comment.

Perhaps his most important point, contained in his last paragraph, is that switch characteristics required to meet conditions of severe vibration, combined with slow rate of actuation, can only be obtained at the expense of differential operating energy greater than that required for satisfactory operation under the less severe conditions of the great majority of switch uses. This observation applies to several methods for meeting severe requirements, which will be discussed.

Perkins has dealt with one means for producing switch characteristics to meet special operating conditions. It is introduction of resilience between operating force and the toggle device, either inherently in the switch mechanism or externally.

The usefulness of such resilience to meet special conditions has long been recognized, and is most commonly provided by a leaf spring mounted on the cover of the basic switch, as shown in Figure 3 of the discussion. This construction not only provides resilient actuation, but more frequently is used as a compact, low cost means for securing overtravel of actuating motion and protection of the switch against excessive actuating force. Such leaf springs, or equivalent coil springs, are combined with switch construction details to provide the desired pressure between contact immediately after snap-over, as described by Perkins.

The claims made for a form of mechanism which may embody inherent actuating resilience, is that with such construction snap motion begins before pressure between contacts falls to zero. This is theoretically true, but is not unique to the mechanism described, for the same result occurs when any of several other forms of snap-acting mechanisms are actuated by means suitably balanced in resilience with the respective toggle mechanisms. This is true because of the drooping characteristic of the curve of operating motion versus operating force.

In any such case the amount of sustained contact pressure, which can practicably be accomplished by the critical balance Perkins discusses, is so small as to lack significance. The beneficial effect which does result from the use of resilience is probably due to continuous contact motion, once separation has begun. This reduces chattering, due to vibration, and causes clean break on slow actuation.

Successful use of switches without introduction of actuating resilience, in the great majority of their many uses, is probably due to the effect of minute welds which are broken only when positive contacts separating force exists. The effect of such welds on switch operating differential, as shown in Figure 5 of the paper, is often less objectionable than the increased operating energy differential requirement which results from introduction of resilience, either external or inherent in the switch structure.

For these reasons, I believe that there is no magic in any particular snap spring of this general type, but that the choice of mechanism is dictated by the ease and practicability of adjusting it to provide the operating characteristics desired for any given use. This point is of considerable importance, as it is probable that the wide and varied use of this type of switch is largely due to the ready availability of switch characteristics which fit closely each particular job.

It is possible that the advantage of providing resilience inherently in the spring, instead of providing it externally only when required, may somewhat limit the versatility of the inherently resilient construction. In particular, the unavoidable higher differential operating energy requirement sets a limit to its use in thermostats or pressure-responsive devices, where the smallest possible operating differential is the requirement. A switch of the construction described cannot be used where only extremely small operating motion is available.

Immunity to the effect of vibration is required only under special conditions, such as mentioned in the paper as occurring where switches are used in thermostats which are exposed to vibration produced by motors or motor-driven fans, supplied from the same power source as that controlled by the switch. In such operation the switch is near the point of snap-over most of the time, and when the switch is moved in one direction on one half-wave and in the opposite direction on the second half-wave, contacts may separate on one phase and close on the other to produce a rectifying action which seriously injures contacts. In most cases this difficulty can be overcome by reorienting the switch mounting relative to the direction of vibration. A more general solution is to mount the moving switch contact on an auxiliary resilient member, as shown in Figure 4 of the discussion. This arrangement allows the moving contact to follow motion of the fixed contacts, when vibration exists, in such a manner as to avoid chattering and the rectifying action. This construction also maintains contact pressure up to the instant snap motion begins, but the full reason for its success in many installations where it serves well in the presence of severe vibration is no doubt quite complex for it involves the natural period of response of the entire system. Satisfactorily close, differential performance in thermostatic uses has been experienced with this type of contact mounting.

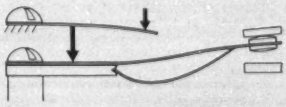


Figure 3. Energy stored in a resilient actuator accomplishes maintained contact pressure up to the point of snap at the expense of increased differential operating energy

Figure 4. Suitable resilient contact mount maintains contact pressure until snap begins, and resists the effect of vibration

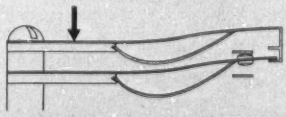


Figure 5. Double snap construction offers the highest resistance to vibration and impact

There are applications where neither of the methods shown in Figures 3 or 4 of the discussion provide the necessary resistance to the effects of vibration or impact. A particular instance is that of a switch used in an air pressure control on aircraft. The switch is mounted directly on a compressor motor running 1,500 rpm, where the excursion of vibration is material. Ample operating energy is available in this instance, and the structure schematically shown in Figure 5 of the discussion performs satisfactorily. Here a mechanical snap mechanism drives a snap-acting switch of the type which remains in one or the other of the positions until driven to the opposite. Positive pressure between contacts exists until the hammer-blow action, provided by lost motion in the linkage, accomplishes rapid separation. In this type of switch, the effect of minute contact welds does not appear in operating force, movement, or differential changes.

This same type of mechanism is successfully used to produce simultaneous make and break in multipole switches such as are coming to be widely used on aircraft. One example of a compact switch of this type is illustrated in Figure 11 of the paper.

I appreciate Johnson's remarks and stimulating suggestions.

Contact bounce is a subject of great importance, for the study of which special equipment has been developed. At the present time it appears that a solution may lie in quite a different direction from the resilient mounting of fixed contacts. The encouraging results of tests now underway are not, however, mature enough to discuss.

Johnson's timely remarks on the usefulness of rectifiers for arc extinction on direct current, may be supplemented by reference to a paper by Dallas and McCully,[1] which deals with extension of contact life by arcless interruption of 50 milliampere loads at 120 volts direct current.

A question was asked from the floor regarding Underwriters' listing of switches of this general type, particularly as to such listing under Industrial Standards. Most such listings have been under the Special Use Snap Switch Standard,[2] which in reality does not at all fit precision, snap-acting switches, as they are distinctly different in both construction and the field of application from the manually operated toggle switches around which the standard was written. It is believed that a new standard may be provided at an early date under which this distinctly different type of switch will be listed by the Underwriters. In the meantime, at least two manufacturers list precision, snap-acting switches under the Industrial Standard covering switches for one horsepower and less rating.

Reference

1. Sensitive Relay Contact Protection Systems, J. P. Dallas, T. H. McCully. AIEE Transactions, volume 67, Part II, 1948, pages 1204-7.

2. See reference 2 of the paper.

Speed Governing Design Considerations for Multivalve Condensing Steam Turbine-Generators

J. W. BARNETT
NONMEMBER AIEE

THE PROBLEM of the proper approach to correct speed governing system design was little understood during the early days of central station turbine generators. As central station plants expanded, capacities and interconnection of distribution systems took place and the problem of governing became more complex to the designer and more critical to the central station systems. Early slow and cumbersome governing systems had to be replaced by more rapid and flexible systems. The trend from 1,800 rpm turbine generators to 3,600 rpm units of mounting pressures and temperatures has required that governing systems become increasingly rapid and more responsive to minute system frequency changes.

Turbine-generator manufacturers had tried previously to meet governing specifications as requested by the purchasers for their individual system requirements. No standard requirements had been set up to assist the purchaser in determining what governing requirements could readily be met by the turbine-generator manufacturers and should be required to be met by them for satisfactory governing. Such a concrete set of requirements had been attempted at intervals, but it was not until the joint committee of AIEE and ASME issued a Recommended Specifications for Speed Governing of Prime Movers Intended to Drive Electric Generators[1] that this need was fulfilled. These specifications have recently been further expanded and now cover all steam turbine-generators intended to drive units of 500 kw and above.

A speed governing system is described in this paper, consisting of a rotating pilot valve governor, a valve servo-motor of the skirted piston design, and cam-operated governor-controlled valves, and comprise one type of system which satisfactorily meets all requirements as specified in Recommended Specifications for Speed-Governing of Steam Turbines Intended to Drive Electric Generators Rated 500 Kw and Up.[2]

Governor-Controlled Valves

A speed governing system suitable for controlling a typical condensing turbine generator is shown in Figure 1. This mechanism consists of a symmetric set of upper and lower cam-operated governor-controlled valves of the poppet design with venturi seats. The valve disks have freedom of motion relative to their stems and are provided with a spherical surface to insure tight seating. The valves are operated in proper sequence by the cams to give a load curve of kilowatts versus valve servo-motor stroke which approaches a straight line. The camshaft is positioned by the valve servo-motor through a rugged mechanism using a rocker-arm sector and pinion arrangement.

Valve Servo-Motor

The valve servo-motor is positioned by a double-acting secondary pilot valve capable of admitting high pressure oil to the operating piston of the valve servo-motor. The valve servo-motor is so designed that rapid closing time is assured by providing a skirt on the power piston in order to decrease the piston area in the closing direction to about 50 per cent of the area provided in the opening direction. The power required in the closing direction is much less than that required in the opening direction, because the springs of the individual governor-controlled valves and unbalanced steam forces in the closing direction of the governor-controlled valve disks assist the closing of the valve servo-motor. For a given required closing time of the valve servo-motor, the skirted piston arrangement requires that the main hydraulic pump capacity be only one-half of that required for a valve servo-motor without the skirt. Eliminating the skirt materially increases the power requirements of the pump as well as its physical size. This would not be desirable in the large size units, since it would represent a considerably increased fixed loss to the purchaser.

A valve servo-motor with the skirted piston arrangement will require an opening time twice that of the closing time. This increased time of opening is quite satisfactory since it is not desirable to open the valve servo-motor at the same rate as required in the closing direction.

Speed Governor

The speed governor is the heart of the speed governing system and upon its sensitivity depends the turbine generator's ability to follow system frequency changes. The basic element of the speed governor is the rotating pilot valve. This pilot valve is positioned in a vertical plane by a speed-sensitive governor head driven from the main turbine shaft through a worm and worm wheel. High pressure oil from the main pump is

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J. W. Barnett is with the steam turbine engineering division of the General Electric Company, Schenectady, N. Y.

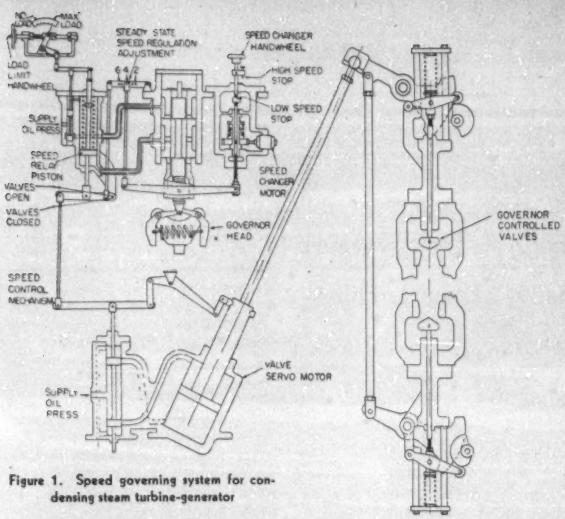


Figure 1. Speed governing system for condensing steam turbine-generator

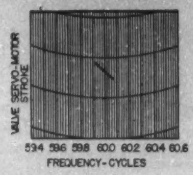


Figure 3. Speed governing system sensitivity

admitted through a load limiting pilot valve to the sliding bushing of the governor rotating pilot valve. The oil passes through the feed port of the bushing into the rotating pilot valve, and then to the single acting speed relay piston. Within the range of the governor setting, the rotating pilot valve possesses the ability to admit high pressure oil to the speed relay piston which accurately positions the speed control mechanism and admits steam through the governor-controlled valves.

Figure 2 shows a typical speed governor fully enclosed within the turbine front standard. The relation of the speed governor to the other component parts of the speed governing system is clearly shown.

Starting Handwheel and Load Limit

When the turbine-generator is at rest or coming up to speed, the governor head weights are against their inner stops and the rotating pilot valve is unable to position the speed relay piston because the feed ports in the sliding bushing will be wide open until the action of the weights in the governor head displaces the rotating pilot valve enough to throttle the oil at the bushing feed port. To take care of the starting condition, the starting handwheel which is mechanically linked to the load limiting pilot valve positions the speed relay piston. Once the rotating pilot valve takes control of the speed governor, the handwheel may be turned to its wide open position. The handwheel and auxiliary pilot valve may also perform a second important function. The mechanism may be used as a load limit under normal operating conditions and will limit the amount of speed relay piston travel in the opening direction to limit the load. This setting will not interfere with the rotating pilot valve's ability to unload the machine in response to high system frequency.

Speed Changer

At about 89 per cent speed the turbine-generator reaches the speed governor setting, and at this point the speed changer may be used to control the machine either manually at the turbine or motor-operated from the electrical control room. Two stops are provided on the speed changer. One provides an upper limit of governor setting and the second, the lower limit. Electrical limit switches are provided at each extreme of travel of the speed changer to operate signal lights for the benefit of the electrical control room operator. These switches are

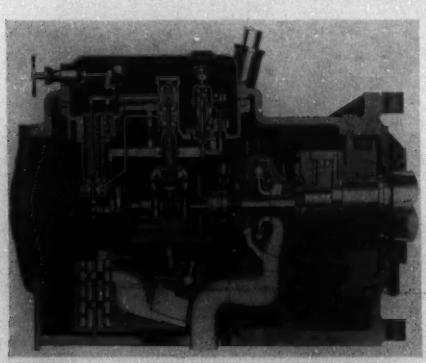
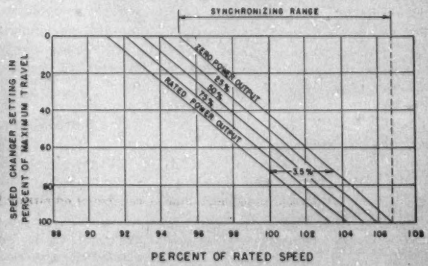


Figure 2 (left). Speed governor located in turbine front standard

Figure 4 (below). Steady-state speed regulation



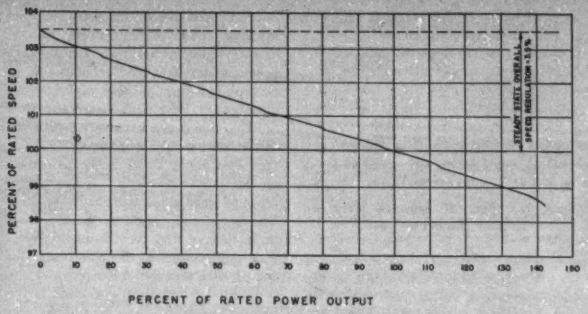


Figure 5 (A). Speed-power output curve with speed changer set to give rated speed at rated load

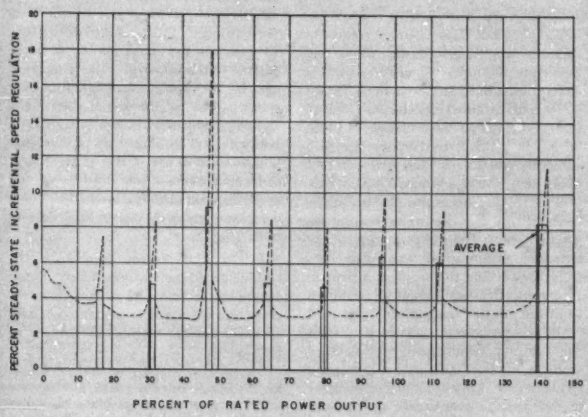


Figure 5(B). Steady-state incremental speed regulation

not intended to break the motor circuit, but built into the speed changer mechanism is a friction clutch which slips when either stop is reached. The speed changer motor is of a sufficient size that it only draws a fraction of rated current when either stop is reached and the clutch slips.

Manual Overspeeding

Built into the stop which provides the upper limit of governor setting is an overspeed button latch which permits the turbine operator to use the manual synchronizing handwheel to run the turbine under control of the governor right up to the tripping speed of the emergency governor simply by pulling up on the button and turning the handwheel beyond the upper limit stop. When the speed changer mechanism is turned back to the normal operating range, either with the manual handwheel or the motor, the stop button automatically relatches.

Speed Governing System Sensitivity

The sensitivity of the rotating pilot valve governor is kept to a maximum due to the continuous rotation of the element which tends to eliminate friction. The rotational effect tends to prevent foreign particles from lodging between the rotating pilot valve and the sliding bushing. The continuous flow of leakage oil through the close clearance between the pilot valve and bushing also contributes to washing away minute foreign particles as well as lubricating the two surfaces. Due to the high sensitivity of the governor, the speed governing system as a whole exhibits excellent over-all sensitivity.

Figure 3 shows a sensitivity chart taken by adapting a movable carriage to a photoelectric frequency recorder. Frequency changes are recorded across the chart and valve servo-motor piston movement is mechanically multiplied to position the carriage. A speed governing system which exhibits poor sensitivity or a considerable amount of dead-band would trace a wide loop on such a chart. It is the width of the loop that is the measure of the sensitivity of the system. This turbogenerator showed the speed governing system dead-band to be 0.03 per cent of rated speed. It should also be noticed that the steady-state incremental speed regulation is also shown by the average slope of the loop and may be calculated quite easily since the multiplied movement of the servo-motor piston is shown for a definite frequency change.

Steady-State Speed Regulation

The steady-state speed regulation on the rotating pilot valve governor may be adjusted easily with the turbine generator at rest by simply changing the relationship of the fulcrum point of the main sliding bushing restoring lever. A set of ten washers are provided at the adjustable fulcrum position capable of being arranged to give a steady-state speed regulation of from 2 to 6 per cent. In general, if the purchaser has expressed no preference, the governors are set to give a steady-state speed regulation of 3.5 per cent rated speed from zero power output to rated power output. Figure 4 is a typical curve showing this setting.

Steady-State Incremental Speed Regulation

Even though the speed governor and speed control mechanism are correctly designed, if proper consideration is not given to correctly designing the cams for lifting the governor-controlled valves, the speed governing system in general will not be satisfactory. Poor cam design can cause unequal amounts of load to be picked up for equal movements of the valve servo-motor. It is essential that cams be designed to give essentially a straight line characteristic of turbine power output versus valve servo-motor piston stroke. Even though great care is

exercised in the cam design, the steady-state incremental speed regulation will be increased at the intercept point of each valve. In the interest of turbine efficiency, it is desirable to lift a valve a sufficient amount to minimize the pressure drop through the valve before the next valve starts opening. Figures 5A and 5B show a very excellent job of cam design, and the steady-state incremental speed regulation varies but a small fraction of a per cent from the steady-state speed regulation, except at the intercept points and at the beginning of the number 1 cam which was designed in this fashion for easy synchronizing.

Overspeeding on Loss of Maximum Power Output

Contracts written for large turbine generators specify that the speed governing system be capable of operating sufficiently fast upon dropping maximum load from the unit so the terminal speed reached will not be high enough to trip the emergency governor. The emergency governors are in general set to trip between 110 and 111 per cent speed. With the growing trend to 3,600 rpm machines and larger capacities, the relative masses per kilowatt output of the turbine rotors and generator fields have decreased due to improved design refinements. The resulting smaller moments of inertia of the rotating parts with increased kinetic energy storage has resulted in much faster rotor acceleration upon loss of maximum load. On many large units, the rotors will accelerate in excess of 15 per cent overspeed in one second. This means that if the emergency governor were set at 10 per cent overspeed, the speed governing system would have to function sufficiently fast to close the governor-controlled valves in slightly less than 0.7 seconds. This problem would not be difficult if at the same instant the governor head felt the speed change, the servomotor began closing the governor-controlled valves. From a practical point of view this is impossible because each element of the speed governing system inherently possesses qualities that contribute to a lagging of one element of the mechanism behind the preceding device. Each additional increment of time makes the closing problem more difficult. Time is consumed in accelerating the weights of the governor head a sufficient amount to displace the governor rotating pilot valve an amount to cause the speed governor relay to dispell the oil under its piston at the maximum rate. More time is required to get the main pilot valve

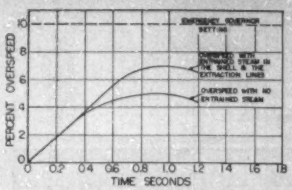


Figure 6. Effect of entrained steam on turbine-generator overspeed

approximately 50 per cent open, so the maximum rate of oil flow into the closing side of the servo-motor will be realized.

Effect of Entrained Steam on Overspeed

Even when the control valves are actually closed, there is still a volume of steam in the turbine casing which will accelerate the unit. Figure 6 shows the effect of entrained steam on an 80,000 kw 1,800-rpm turbine-generator for a load dump of 96,000 kw. This entrained steam, in general, contains energy of a sufficient amount to cause one per cent overspeed. More entrained steam capable of feeding back into the turbine is contained in each feedwater heating extraction line. The energy storage of the entrained steam in all extraction lines from the turbine to the check valves in their respective lines cannot be permitted to exceed another one per cent overspeed if the speed governing system is to meet the contract requirements. A detailed study is made of the station extraction system to see if it will satisfactorily allow the contract commitments to be met. Recommendations are made for revised check valve locations if the entrained steam volumes exceed the allowable one per cent overspeed. In cases where an auxiliary steam source connects into an extraction line for feedwater heating purposes under low-load conditions, in general, it is required that a plain swing check valve be placed in series with a positive-closed check valve between the steam source and the turbine. It is felt that destructive overspeed would result on the turbine-generator if full load were dropped and a check valve failed to close. This is guarded against by the additional protection of the second check valve. The positive-closed check valve is required to be arranged for tripping by the emergency governor on overspeed of the turbine generator. The entrained steam problem may be further complicated by the omission of large cumbersome check valves difficult to justify in one or more low pressure extraction lines which feed closed feedwater heaters. In this case the entrained steam volume will include the steam in the extraction line from the turbine to the heater plus the steam storage in the heater shell proper. Any water stored in the heater presents a flashing problem and must be kept to a minimum. This effect may be decreased if the water is not stored in the heater shell proper but is contained in a drain cooler connected to the heater shell by a comparatively small pipe to limit the rate of flashing.

Briefly, the present practice of the turbine-generator manufacturer, represented by the writer, regarding the use of nonreturn check valves in extraction lines of nonautomatic extraction units, is as follows:

1. No check valve required. (When the total energy stored in the heater shell plus its extraction line is negligible. That is, the sum total of this energy plus the energy contained in all other extraction lines feeding back into the turbine will not cause an

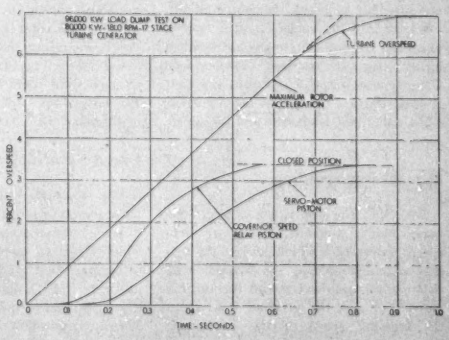


Figure 7. Speed governing system speed-time lag for instantaneous loss of maximum power

overspeed on the unit in excess of one per cent upon loss of maximum load.)

2. One free swing check valve. (Used when a failure of this valve on loss of maximum load would result in a back flow into the turbine capable of accelerating the unit not in excess of 20 per cent overspeed momentarily.)

3. One positive-closed check valve. (Used when a failure of this check valve on loss of maximum load results in back flow from a limited energy source such as an open heater, evaporator, or deaerator, capable of causing a momentary maximum speed in excess of 120 per cent.)

4. One positive-closed check valve and one free swing check valve in series. (Used when there is a steady source of steam supply from a boiler, reducing station, boiler feed pump exhaust, or extraction connection from another turbine, capable of running the turbine continuously in excess of 120 per cent speed upon loss of maximum load.)

Effect of Speed Lag on Components of the Speed Governing System

Figure 7 shows an actual load dump test as recorded by oscillographs for this same 96,000 kw load dump.

This rotor only had a measured acceleration rate of 9.1 per cent overspeed per second, but it will give an accurate picture of the rotor acceleration and the various time lags between the elements of the speed governing system. During the time immediately following the load dump, the inertia of the governor, force lag of the weights, and position lag of the speed relay piston cause the speed governor to lag behind the ideal steady-state condition corresponding to the 3.4 per cent regulation for the speed changer nut setting. The speed governor lag, in turn, reflects upon the instantaneous position of the main pilot valve. The pilot valve was not unported sufficiently to have the servo-motor piston reach its maximum rate of acceleration until 0.275 seconds after the load dump. The speed governor had a maximum lag of 1.3 per cent speed at the beginning of the relay stroke and 1.85 per cent speed lag at the end of the stroke. When the speed governor pilot valve was effectively opened, the minimum lag was 0.73 per cent speed and the relay cylinder was closed 0.57 seconds after the instant of dumping the load. The valve servo-motor piston reached an acceleration equal to the rotor acceleration approximately 0.25 seconds after the instant of load dump and had a maximum lag of 3.5 per cent speed when the valve servo-motor reached its closed position. The valve servo-motor was closed 0.825 seconds after the instant of dumping the load. A maximum rotor overspeed of 7 per cent was measured with the rotor showing a slowing-up effect from the maximum acceleration rate 0.65 seconds after the instant of dropping the load.

The foregoing values represent a satisfactory governing mechanism performance for a turbine-generator whose rate of acceleration under maximum load condition is 9.1 per cent per second. For rotors having rates of acceleration up to 15 per cent per second, the lag of the valve servo-motor piston behind the governor speed relay piston, illustrated in Figure 7, may be required to be reduced. In double acting servo-motors, this may be accomplished by increasing the oil pump capacity and providing a larger servo-motor pilot valve. Reduction in time lag of the governor speed relay piston behind the rotor acceleration may also be required and can be readily accomplished.

References

1. Recommended Specification for Speed Governing of Prime Movers Intended to Drive Electric Generators. AIEE Standard Number 600, July 1944, 10 pages.

2. Recommended Specification for Speed-Governing of Steam Turbines Intended to Drive Electric Generators Rated 500 Kw and Up. AIEE Miscellaneous Paper Number 48–4.

Discussion

J. R. Norton (General Electric Company, Schenectady, N. Y.): The author's excellent paper is a well illustrated résumé of modern governor design. It is rugged and simple, yet sensitive to an extreme degree without sacrificing safety and reliability. The governor engineers are to be commended for a design which makes the turbine control device an accurate and reliable instrument.

The essentially constant incremental regulation, except at the valve cracking points, approaches the ideal governing so necessary for the economical operation of present-day large and interconnected generating stations. The near-linear response does much to alleviate the duty of automatic frequency controls. The minimum value of the three per cent incremental regulation insures good system stability, and the maximum values should not prove a hindrance to good automatic frequency control. The modern governor is a much needed development that will permit utility operators to take full advantage of the normal and maximum ratings of generators, transformers, and transmission lines. As the installed capacity of accurately governed prime movers approaches the optimum, the more easily block loading is accomplished, and the effect of fringe or unpredictable load swings is minimized.

The optimum in system regulation and tie-line load control can be hastened by a program to eliminate or improve inaccurate governors on major prime movers. Turbine manufacturers are doubtless ready to adapt to existing units this modern development so ably described to us.

Preliminary Considerations of System-Wide Load Division by Means of Accurate Time Control

T. E. CURTIS
ASSOCIATE AIEE

W. M. PICKSLAY
MEMBER AIEE

Synopsis: Generators and tie lines experience damped natural oscillations in response to load changes or other disturbances. The magnitude and duration of such oscillations is considerable when contemplating system-wide load division by means of accurate time control. A computing servomechanism is considered herein for high-speed resolution of phase angle conditions, whether oscillating or not, into a steady load order proportional to the causative load change. Transfer function analysis is included as an essential step in determination of stability of the proposed control system. Such analysis offers a clear picture of the related performance of computer, governor, penstock, turbine, generator, and tie line.

SOME of the attractive possibilities of load division by means of accurate time control have been discussed in earlier papers.[1,2,3] This paper enters further into the studies of the problems to be solved if such control is to be achieved. The method is outlined for theoretical determination of stability through transfer function analysis.[4,5]

Steady State Load Division

The nature of load division under consideration is recapitulated in Figure 1. In steady state, first and second derivatives ϕ' and ϕ'' are equal to zero, where ϕ is the phase angle in electrical radians between generating unit frequency f and primary frequency standard F. Subscripts denote number of generating unit with which the symbol is associated.

In the steady state, the computer outputs or load orders λ_1 and λ_2 are expressed only by $\lambda_1 = c\phi_1$ and $\lambda_2 = c\phi_2$. These are applied to the prime mover governors in such sense that the generator outputs are increased with increasing phase angle. If the generator phase angles are increasingly lagging, it is an indication of insufficient generator output for the connected loads.

Equating power flow at each bus with $G_1 = c\phi_1$ and $G_2 = c\phi_2$:

$$G_1 - L_1 + \frac{E_1E_2}{X_{1-2}} \sin\ (\phi_1 - \phi_2) = 0 \qquad (1a)$$

$$G_2 - L_2 - \frac{E_1E_2}{X_{1-2}} \sin\ (\phi_1 - \phi_2) = 0 \qquad (1b)$$

Assuming E_1 and E_2 are held at one per unit by voltage regulators, and $\sin\ (\phi_1 - \phi_2) = \phi_1 - \phi_2$ when $\phi_1 - \phi_2$ is less than 0.5 radian, equations 1a and 1b reduce to

$$G_1 - L_1 + \frac{\phi_1 - \phi_2}{X_{1-2}} = 0 \qquad (1c)$$

$$G_2 - L_2 - \frac{\phi_1 - \phi_2}{X_{1-2}} = 0 \qquad (1d)$$

The solutions of equations above are plotted in Figure 1, taking tie line reactance as 4 per unit on the base of the capacity of generating unit number 1, $c = 1$ (equivalent to each generator developing rated output at 1 radian lag behind F), L_2 steady at 1 per unit, and L_1 decreasing from 1 to 0.2 per unit.

For this case, approximately 20 per cent of the total load variation appears in the tie line. This would be decreased with higher tie line reactance or by a larger setting of c in the computer. Just how large a setting of c is feasible without resulting instability of control will need be determined by experience. However, it seems entirely practicable to limit the tie line variation to as low as 5 per cent of the change in load at one bus. This figure would be 50 per cent without load control equipment if governors at each terminal had the same droop.

The tie line load curve may be shifted by the addition[3] of Δ to $c\phi_1$ and $c\phi_2$. This is equivalent to adjusting the respective governors to a different division of loads when $\phi_1 = \phi_2$.

Transient Conditions

The previous presentation[2] of this governing system was based upon qualitative considerations primarily concerned with improvements anticipated in steady state load division among generators in an extensive interconnection. In the subsequent quantitative investigation, it has developed that the use of the computing servomechanism offers a further advantage not otherwise realizable, that the phase angle of a generator unit from a reference frequency may be controlled as desired without considerable unnecessary regulation of the gates due to the control trying to follow the instantaneous oscillations in phase angle which occur. The theoretical nature of these oscillations was described by Rüdenberg[6] and Warming.[7]

In Figure 2 (A and B) are reproduced representative 2-minute high-speed charts of the frequency variation experienced at one point in a large interconnection. The solid line shows the instantaneous deviation from 60 cycles, as traced from a frequency recorder chart with greatly expanded scale and suppressed zero. The dotted traces were secured by graphical methods from the solid trace and show the instantaneous rates of change of fre-

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T. E. CURTIS and W. M. PICKSLAY are both with the Pacific Gas and Electric Company, San Francisco, Cal.

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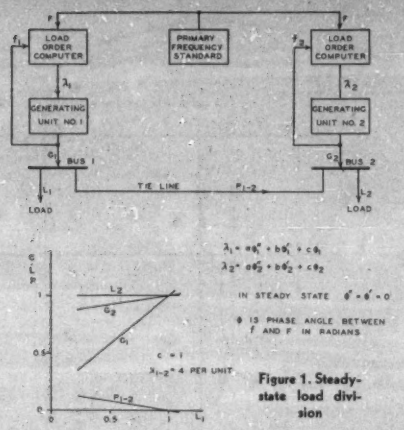

Figure 1. Steady-state load division

quency and the accumulated frequency deviation.

Both curves were obtained in the Safe Harbor Hydro Development of the Safe Harbor Water Power Corporation on a weekday afternoon to secure a fair sample of normal conditions. Curves in Figure 2(A) were with five generators regulating system frequency with the New York tie open, at which time the estimated interconnected capacity was 5,000,000 kw. Curves in Figure 2(B) are with six generators regulating with the New York tie closed, giving an 11,000,000 kw interconnection.

Inspection of these curves shows that random oscillations in bus frequency occasioned by load disturbances are substantially sinusoidal, have a period of approximately 4 seconds and a time constant of 6 seconds, and are substantially the same whether or not the New York tie is closed. This indicates that, for practical purposes, the oscillations are expressible by equation 2. This equation may also be arrived at from logical considerations[8] of system characteristics.

$$J\phi_1'' + \delta\phi_1' + A\phi_1 = T_1 \tag{2}$$

Where

J = inertia constant of connected generators at Safe Harbor.
δ = damping factor.
A = spring constant equivalent to elastance of transmission links between Safe Harbor and the remainder of the interconnection.
ϕ_1 = angle in radians that generator voltage lags voltage of a 60-cycle reference frequency.
T_1 = per unit torque equivalent to load disturbance.

The purpose of the load order computer is to solve equation 2 continuously for T_1. Therefore, if the settings of a, b, and c in the computer are proportioned to the system constants J, δ, and A at the location of the computer, output λ will be a measure of the amount of load change which initiated the oscillation. If λ is applied to the governors, it will cause the local units to absorb the local load change immediately. This means that with computers installed at major generating stations, the effect of load changes would be restricted within the areas in which load changes occur, with transmission links remaining substantially unaffected.

Figure 3 approximates the observations of Figure 2 in terms of the second order linear differential equation 2. This approximation considers that the generators have 4 kilowatt-seconds stored energy per kilovolt-ampere capacity, and that the effective tie line reactance is 18.8 per unit from an infinite bus on the base of connected capacity at Safe Harbor. Factors a, b, and c, computed previously, are listed in Figure 3. Factor c is taken arbitrarily as 1.000 in order to provide load division similar to that shown in Figure 1.

Assuming a block change of a unit load torque as indicated in Figure 3, the resultant oscillations in ϕ, ϕ', and ϕ'' are calculated from the solution of equation 2. If the instantaneous values multiplied by the factors are added, the steady load order λ in the upper curve is obtained. The approximate transient response of the computer is indicated for a computer figure of merit $\beta = -6$. Note that whereas the computer would adjust the governor to the required new value in approximately 1 second without overshooting, a control responsive to phase angle alone would oscillate about the new required value for many seconds.

Computer Performance

A block diagram of the computer is included in the upper portion of Figure 4. Its transient performance is expressed by the differential equation

$$k\phi_1'' + h\phi_1' + \phi_1 = f_1(t) \tag{3}$$

where $f_1(t)$ represents any selected form of computer input. Factors h and k are determined as below for optimum operation of the computer. Equation 3 considers that the input servo has sufficiently fast response to keep the θ contacts operating about the neutral position, and that the contact spacing is negligibly small. For critically damped operation of the computer, it is necessary that

$$\sqrt{h^2 - 4k} = 0$$

The figure of merit of the computer is defined

$$\beta = -h/2k$$

To examine the response of the computer to a damped oscillation as indicated in Figure 3, consider that

$$f_1(t) = \frac{T}{A} - \frac{T}{B} e^{\alpha t} \cos(\psi t - \rho) \tag{4}$$

Where

A is a constant

$$B = \frac{A}{\sqrt{\alpha^2 + \psi^2}}$$

T = torque in per unit
α = damping factor in inverse seconds
ψ = natural frequency in radians per second
ρ = displacement angle in radians

The solutions of equations 3 and 4 are then

$$\phi_1 = \frac{T}{A} - \frac{T\beta^2 e^{\alpha t}}{B[(\alpha-\beta)^2 + \psi^2]} \times \cos\left(\psi t - \rho - 2\tan^{-1}\frac{\psi}{\alpha-\beta}\right) + (c_1 t + c_2)e^{\beta t} \tag{5}$$

$$\phi_1' = \frac{T\beta^2\sqrt{\alpha^2+\psi^2}}{B[(\alpha-\beta)^2+\psi^2]} e^{\alpha t} \times \sin\left(\psi t - 2\tan^{-1}\frac{\psi}{\alpha-\beta}\right) + (c_1\beta t + c_1 + c_2\beta)e^{\beta t} \tag{6}$$

$$\phi_1'' = \frac{T\beta^3(\alpha^2+\psi^2)}{B[(\alpha-\beta)^2+\psi^2]} e^{\alpha t} \times \cos\left(\psi t + \rho - 2\tan^{-1}\frac{\psi}{\alpha-\beta}\right) + (c_1\beta^2 t + 2c_1\beta + c_2\beta^2)e^{\beta t} \tag{7}$$

Where c_1 and c_2 are constants

$$\rho = -\tan^{-1}\alpha/\psi$$

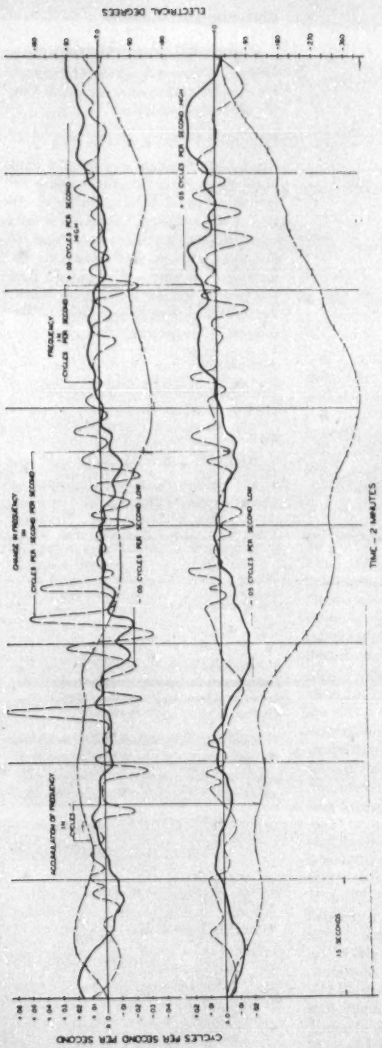

Figure 2(A). Pennsylvania Water and Power Company tests

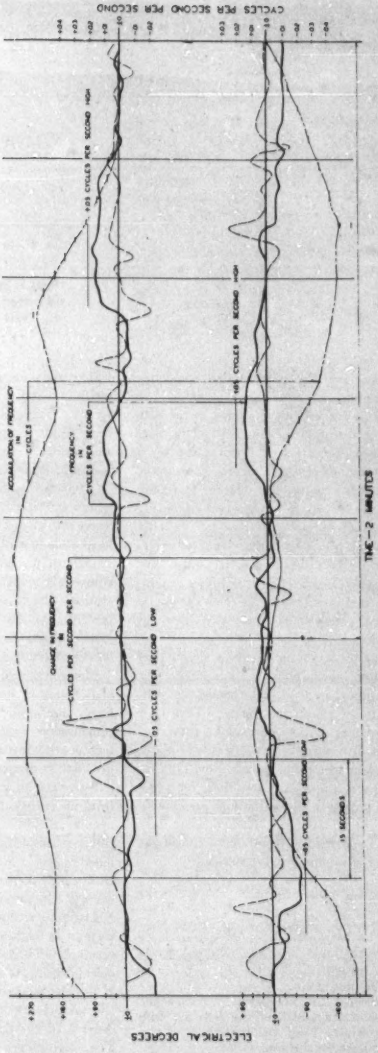

Figure 2(B). Pennsylvania Water and Power Company tests

$$\frac{\beta^2}{[(\alpha-\beta)^2+\psi^2]}$$

is computer amplitude factor

$$2\tan^{-1}\frac{\psi}{\alpha-\beta}$$

is computer lag angle.

These solutions show that the three outputs of the computer, before consolidation into the load order, all lag by the same angle the actual ϕ, ϕ', and ϕ'', respectively. The solutions also show that the computed amplitudes are all reduced from the respective actual amplitudes by the same fraction. The lag angle and the amplitude reduction both decrease as the figure of merit β is increased. The amplitude reduction may be compensated for by gear ratios in consolidating the three outputs into the single load order. It is important that all three outputs lag the actual values by the same angle, because this permits the resolution of the load order with a minimum "ripple."

Frequency Response of Computer

In order to examine stability of the entire control system by means of transfer function analysis, it is necessary to determine the response of the computer when

$$f_1(t) = \sin \psi t \tag{8}$$

where ψ is an assumed variable frequency expressed in radians per second. Equation 8 represents a steady state sinusoidal variation between f and F, such as might be caused by a small sinusoidal variation of the gate position, or by a small sinusoidal variation in the load torque.

The steady state solutions of equations 3 and 8 are

$$\phi=\frac{\beta^2}{\beta^2+\psi^2}\sin\left(\psi t-2\tan^{-1}\frac{\psi}{-\beta}\right) \tag{9}$$

$$\phi'=\frac{\beta^2\psi}{\beta^2+\psi^2}\cos\left(\psi t-2\tan^{-1}\frac{\psi}{-\beta}\right) \tag{10}$$

$$\phi''=\frac{-\beta^2\psi^2}{\beta^2+\psi^2}\sin\left(\psi t-2\tan^{-1}\frac{\psi}{-\beta}\right) \tag{11}$$

These solutions are multiplied by load order constants c, b, and a, respectively, and are plotted in Figure 5. The curves represent loci of vectors with respect to ϕ_o, the actual phase angle between f and F. For example, the $c\phi$ curve indicates that computer output $c\phi$ lags the actual phase angle ϕ_o by approximately 19 degrees when ψ is one radian per second. Computer output $a\phi''$ leads ϕ_o by nearly 180 degrees at low frequencies. The magnitude of $a\phi''$ increases and lead angle from ϕ_o decreases as ψ is increased.

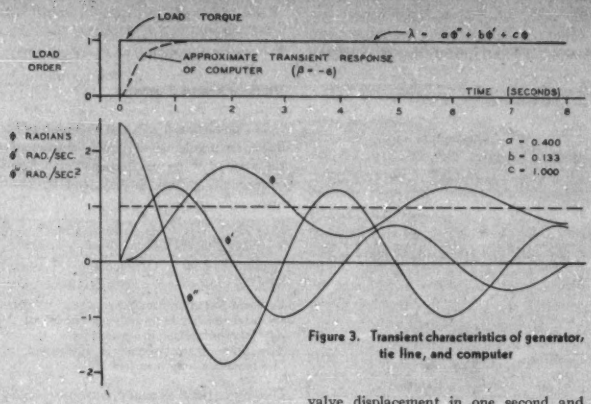

Figure 3. Transient characteristics of generator, tie line, and computer

The vector sum of the three computer outputs is also plotted in Figure 5, and is represented as

$$\lambda=a\phi''+b\phi'+c\phi \tag{12}$$

The over-all output-over-input[5] transfer function becomes

$$Y_{(G)}=\frac{\lambda}{\phi_o}=\frac{(c\beta^2-a\beta^2\psi^2)+j\psi b\beta^2}{(\beta^2-\psi^2)-j2\psi\beta} \tag{13}$$

The family of vector diagrams represented by Figure 5 illustrates that λ may be made to pass through or near the origin at a selected frequency by suitable adjustment of constants a, b, and c. Such adjustment could be likened to "tuning" the computer to the natural frequency of the system. Figure 3 illustrates the optimum "tuning" to the damped natural oscillations which are characteristic of the system for particular conditions. It is noteworthy that ϕ, ϕ', and ϕ'' are separated by phase angles of 90 degrees in the steady state sinusoidal case, as compared to 96 degrees separation as indicated in Figure 3 for damped natural oscillations. The additional six degrees is represented by ρ in equations 5 and 7.

Governor Performance

In order to simplify the following loop transfer functions and the resulting system transfer functions, representative values for constants are inserted during the development.

Referring to Figure 4, the rate of change of relay valve displacement is proportional to the pilot valve displacement, that is

$$S'=K_1\epsilon$$

Assuming K_1 corresponding to unit relay valve displacement in one second and substituting $j\psi$ for the differential sign the loop transfer function becomes

$$\frac{S}{\epsilon}=\frac{1}{j\psi}$$

But the relay valve displacement is used as a cut off for the pilot valve as represented by the unit feedback loop. Therefore

$$\frac{S}{i}=\frac{S/\epsilon}{1+S/\epsilon}=\frac{1}{1+j\psi}$$

The rate of change of turbine nozzle opening is proportional to relay valve displacement, that is

$$G'=K_2S$$

Assuming unit nozzle opening in four seconds with the relay valve at unit displacement, then

$$\frac{G}{S}=\frac{1}{4j\psi}$$

and

$$\frac{MG}{i}=\frac{M}{1+j\psi}\frac{1}{4j\psi}$$

but

$$\frac{MG}{\lambda_c}=\frac{MG/i}{1+MG/i}=\frac{M}{(1+j\psi)(4j\psi)+M} \text{ or}$$

$$\frac{G}{\lambda_c}=\frac{1}{[(M-4\psi^2)+j4\psi]}$$

where $M=K_3D$ and K_3 is the linkage ratio between the droop setting mechanism and the pilot valve and D is the per unit droop setting.

The performance of the dashpot is characterized by the equation

$$x^1+\frac{x}{T_1}=RG'$$

where R includes all linkage ratios through

the dashpot up to the pilot valve. Assuming a dashpot time constant T_1 of one second and substituting $j\psi$ as before:

$$\frac{x}{G}=\frac{j\psi R}{1+j\psi}$$

Then

$$\frac{x}{\lambda_c}=\frac{x}{G}\frac{G}{\lambda_c}=\frac{j\psi R}{1+j\psi}\frac{1}{(M-4\psi^2)+j4\psi}$$

and

$$\frac{x}{d}=\frac{x/\lambda_c}{1+x/\lambda_c}=\frac{j\psi R}{(1+j\psi)(M-4\psi^2+j4\psi)+j\psi R}$$

But

$$\frac{G}{x}=\frac{1+jx}{j\psi R}$$

Therefore

$$Y_{(G)}=\frac{G}{d}=\frac{1+j\psi}{(M-8\psi^2)+j\psi(R+M+4-4\psi^2)} \tag{14}$$

This is the governor system transfer function. Considering flyball input as part of $b\phi'$, this is plotted in Figure 6 for $M=1$ and $R=50$. The complete formula without the substitution of numerical constants is

$$Y_{(G)}=\frac{K_1K_2(1+j\psi T_1)}{[MK_1K_2-\psi^2(1+K_1T_1)]+j\psi[K_1K_2T_1(R+M)+K_1-T_1\psi^2]} \tag{14a}$$

Penstock and Turbine

The deviation G in nozzle opening produces an acceleration or deceleration of the mass of water in the penstock. The corresponding force is balanced by the reaction of the jet, that is

$$\frac{\delta L_pA_pV_p'}{32.2}=-\delta A_jh \tag{15}$$

where

h = deviation in head in feet of water
δ = water density in pounds per cubic foot
A_j = jet cross section in square feet
A_p = penstock cross section in square feet
L_p = penstock length in feet

This equation assumes incompressible water and a rigid penstock and is permissible where

$$\frac{1}{\psi G}>\frac{4L_p}{V_s} \tag{16}$$

where V_s is the velocity of sound in water or approximately 4,700 feet per second.

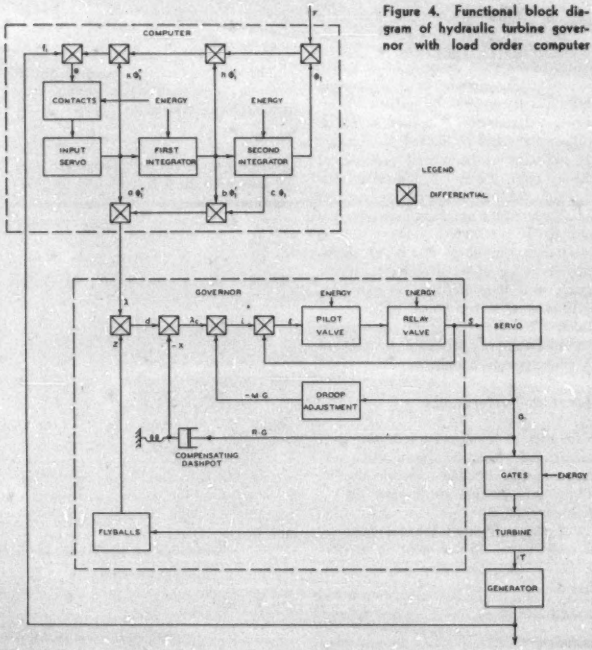

Figure 4. Functional block diagram of hydraulic turbine governor with load order computer

Dividing both sides of equation 15 by V_oH and remembering that $A_j/A_p=1+G$, then

$$\frac{L_pV_o}{32.2H}v_p'=-h(1+G)$$

with v_p' and h now in per unit values.

Let

$$\frac{L_pV_o}{32.2H}=T_2 \tag{17}$$

where T_2 is the penstock time constant in seconds and V_o and H are initial penstock water velocity and head respectively.

After substitution

$$T_2v_p'=-h(1+G)$$

The jet velocity $V_j=\sqrt{2g(H+h)}$. However, we may assume $h=1.9V_j$ where the deviation in jet velocity from unit velocity is less than 0.1 per unit.

Then

$$T_2v_p'=-1.9\ V_j(1+G)$$

But

$$V_j=\frac{1+v_p}{1+G}-1$$

Therefore

$$T_2v_p'+1.9V_p=1.9G$$

or

$$v_p=\frac{1.9G}{1.9+j\psi T_2}$$

and

$$v_p'=\frac{1.9Gj\psi}{1.9+j\psi T_2}$$

In the per unit system we may say that $1+v_p=Q$ the penstock flow. Then

$$Q=1+\frac{1.9G}{1.9+j\psi T_2}$$

The deviation in head

$$h=-\frac{T_2v_p'}{1+G}$$

Substituting, multiplying numerator and denominator by $1-G$, and dropping terms in G squared permissable since G is assumed not to exceed 0.1 per unit, then

$$h=-\frac{T_2G1.9j\psi}{1.9+j\psi T_2}$$

and the total head

$$H=1-\frac{T_2G1.9j\psi}{1.9+j\psi T_2}$$

The power available in the jet is QH. This is also turbine torque at unit wheel velocity considering the turbine as 100 per cent efficient.

Since

$$QH = 1 + \frac{1.9G - j\psi 1.9T_2G}{1.9 + j\psi T_2}$$

again dropping terms in G squared.

The torque deviation

$$T = \frac{1.9G - j\psi 1.9T_2G}{1.9 + j\psi T_2}$$

and

$$Y_{(P)} = \frac{T}{G} = \frac{1.9 - 1.9j\psi T_2}{1.9 + j\psi T_2} \tag{18}$$

This penstock transfer function is plotted in Figure 8 with T_2 equal to one second calculated by equation 17 from representative data,[8] for a penstock 690 feet long with rated velocity and head of 10.25 feet per second and 213.5 feet of water respectively for a 70,000 horsepower turbine.

In the case of a steam turbine, there being little energy storage in steam flow, an entirely different transfer function would be found. This is not developed however since it presents an easier control problem than does a hydraulic turbine.

Generator and Tie Line

From inspection of equation 2, the generator and tie line transfer function is written

$$Y_{(B)} = \frac{\phi_0}{T} = \frac{1}{(A - J\psi^2) + j\psi\delta} \tag{19}$$

This is plotted in Figure 6 with values for J, A, and δ corresponding to the computer constants a, c, and b respectively shown on Figure 3 so as to simulate the conditions at the Safe Harbor hydro plant.

Complete Loop

The complete loop transfer function is the vector product of the individual transfer functions, that is

$$[Y_{(C)} + Y_{(F)}]\,Y_{(G)}\,Y_{(P)}\,Y_{(B)} \tag{20}$$

where $Y_{(F)}$ is the transfer function of the governor flyballs.

Present day governor heads have time constants on the order of 0.05 seconds or less, corresponding to approximately three degrees lag at one radian per second. The inertia of the flyballs may then be neglected and the flyball transfer function written as:

$$Y_{(F)} = \frac{Z}{F - f} = \frac{j\psi}{377 \cdot W} \tag{21}$$

where W is the per unit deviation in frequency required to produce unit pilot valve displacement and 377 radians per second corresponds to unit speed.

Assuming W as 0.15 per unit, the magnitude of this transfer function is 10 per cent or less of the computer transfer function, depending on ψ at the point considered. Since the addition is made vectorially, $Y_{(C)} + Y_{(F)}$ is only slightly different from $Y_{(C)}$. Also this small difference may be compensated for by reducing the value of b in the computer proportionately since $b\phi'$ corresponds to Z in effect.

Consequently $Y_{(F)}$ may be neglected and the over-all loop transfer function reduces to

$$Y = Y_{(C)}\,Y_{(G)}\,Y_{(P)}\,Y_{(B)} \tag{22}$$

This is the vector product of the individual transfer functions of Figures 5, 6, 7, and 8, and is plotted in Figure 9.

Since the locus falls inside of the -1 point, the system is stable. The effective damping ratio and degree of stability may be estimated from the radius of the circle, centered on the -1 point, which is tangent to the output vector locus.[4] This circle is shown in Figure 9. In the present example, the effective damping ratio is approximately 0.2. This is as low a damping ratio as can be permitted. The ratio can be increased by increase in the value of R associated with the governor dashpot.

The speed of response of this control should be adequate since the over-all gain is not reduced far below one until a frequency ψ of four to five radians per second is reached. This is well beyond the frequency of the oscillations measured at Safe Harbor. However, for abnormal conditions where large and rapid increments of generation may be required, it may be advisable to increase this response automatically by means of a pressure relief valve in the governor dashpot. Such a relief valve would open only in response to large abrupt changes in the load order.

Inspection of Figures 5 and 9 reveals that the effective damping ratio can be increased also by a decrease in the setting

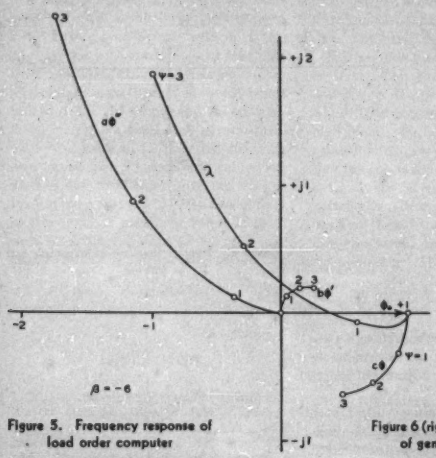

Figure 5. Frequency response of load order computer

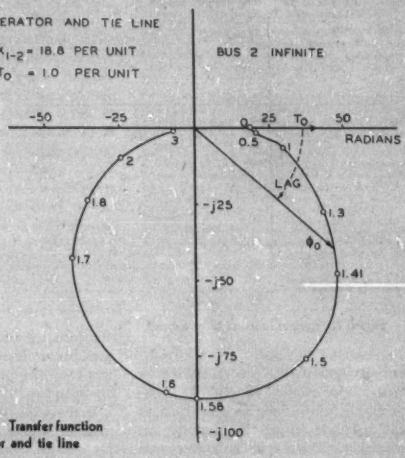

Figure 6 (right). Transfer function of generator and tie line

of b. This action may be visualized by remembering that Figure 9 indicates that the closing of the control loop causes a slight change in the frequency of oscillation and in the effective damping.

Summary

For certain assumed conditions, it has been shown that the computer output is proportional to the load torque initiating the oscillations in generator phase angle. However, if the natural frequency of the oscillations change because of relaying or change in generation in a controlled area, the proportionality factor will change and there will be a ripple in the computer output. As change in generation or transmission connections would be at dispatcher's instructions, such instructions could include setting the computer accordingly. This should not be onerous as dial settings for factors a and b may be determined in advance for each normal system setup.

In the case of relaying, the ripple in the computer output would only delay the restoration of steady state load division. However, the change in the generator and tie line transfer function will modify the stability and a sufficient margin of stability for expected relaying must be incorporated in the governor settings. If the controlled generation should become isolated, infinite tie line reactance, the system becomes absolutely unstable and the computer must be automatically disconnected leaving the governor alone in control.

The tests at Safe Harbor, Figure 2 (A and B), indicate relative absence of other frequencies in the oscillations of generator phase angle. This might be expected since such other frequencies must arise from remoter controlled areas. The presence of the inertia of the nearest controlled area would be expected to damp out such frequencies before they reach the point under discussion. Other frequencies present to a limited degree however would only modify the computer output slightly. This would affect stability, but could not result in hunting since the transfer function analysis shows the system to be stable at any frequency.

Since the computer gain is so much greater than that of the flyballs as indicated by the ratio of their respective transfer functions, it is necessary to increase the lever ratio between gate motion and the dashpot to secure stability. This may require modification of the standard hydraulic governor though a less pessimistic development or experience may show that present adjustment ranges are adequate.

If the adjustment range should need material extension, an additional dashpot which could be disconnected when control is returned to the governor would be desirable. An alternate not requiring dashpot modification would be the use of mechanical or electrical lead networks associated with the computer. Use of electrical lead networks for stabilization in this regard has been discussed by Chestnut.[8]

The foregoing development assumes a governor built for 5 per cent droop. All mechanism ratios are considered reduced to a per unit basis, so that a steady one per unit change in the load order will cause a steady one per unit change in generation. If it is found desirable to retain the flyballs in an installation, their contribution should be co-ordinated with the computer so that the effective $b\phi'$ yields performance as indicated in Figure 3.

The transfer analysis brings to light a hitherto not generally recognized point. It is granted that the inertia represented by the volume of water in the penstock is usually much larger than that of the turbine and generator. However, from the regulation point of view, the combination to the penstock, turbine, and generator is only twice as difficult a problem at most as compared with the turbine and generator alone as may be seen from inspection of the plots of Figures 6, 8, and 9. It is actually the generator and tie line transfer function that principally determines the gain at 180 degrees total phase shift since it shifts nearly 90 degrees for a change of frequency from 1.3 to 1.7 radians per second. The major importance of the hydraulic inertia remains in the determination of permissibles rates of gate motion without physically endangering the penstock and associated structures.

It will be noted that the over-all loop transfer function is plotted with a dashpot time constant of one second whereas previous experience would dictate an optimum value of about seven seconds. Inspection of equation 14a shows that with R so large, T_2 appearing in both numerator and denominator has relatively little effect. In this case R is large to secure

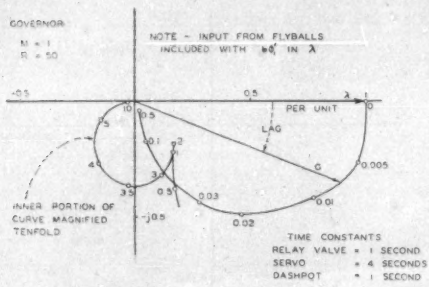

Figure 7. Transfer function of governor

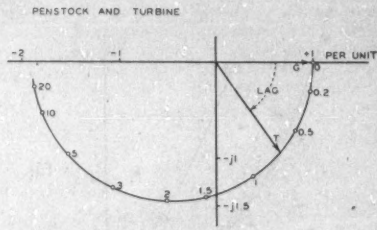

Figure 8. Transfer function of penstock and turbine

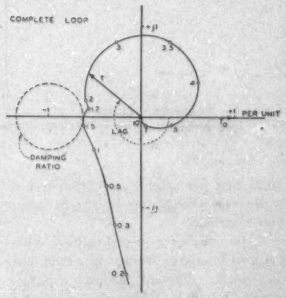

Figure 9. Complete loop transfer function

stability against an infinite bus. An infinite bus is normally a fiction but in this case is physically realized since generation elsewhere on the system is controlled similarly. That is the controls function to maintain a fixed frequency irrespective of load.

For simplicity of analysis, it has been assumed that the generator shaft and terminal voltage angles are one and the same. Further study will be necessary to evaluate the relative merits of computer input from shaft angle versus generator terminal voltage. It would appear though that the extra complication of obtaining shaft angle would not be warranted.

It may be argued that a control of this sensitivity will result in excessive operation of the governor and gate or nozzle mechanisms with corresponding increased wear and maintenance. From an overall point of view though, it should result in less rather than more individual regulating operations since each governor will only need respond to load changes in its own area. Where such control is not used, every unblocked governor on the system must respond to every load change on the system excepting only those of such small magnitude as to call for a load change less than the governor sensitivity as may occur at a regulating unit on the system periphery.

Conclusion

A phase angle control comprised essentially of a computing servo-mechanism which produces a load order proportional to load changes in a controlled area is entirely practicable from theoretical consideration of test data. Such control would not involve major changes in existing equipment, would be far more sensitive than present controls, will improve steady state load division among units in an interconnected system, minimize tie line load fluctuations, and permit operation closer to stability limits. Such control may be stabilized readily except in the event a unit becomes isolated.

It is possible that interconnections may be held intact through greater disturbances, since the proposed control system will initiate accurate corrective action at high speed. Such a control system could operate intelligently even though separation into parts did occur.

Equations have been presented which permit calculation of specific cases. Solutions of these by means of a differential analyzer would give further insight into the operation. Failing such analysis, the next step requires trial of such control on an actual system.

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Discussion

C. K. Duff (Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada): The proposed control system is a daring concept demanding unprecedented sensitivity in the control equipment and speed of response on the part of controlled generators. Admitting the sensitivity of the proposed control equipment and its ability to produce a load order proportional to load changes in a controlled area, the form which this load order takes, and the manner in which it is to be applied to conventional governors to secure the necessary speed of response without major changes in existing equipment, have not been made clear. If as implied in Figure 4 of the paper, control is applied at the same point as manual control and other automatic controls are commonly applied, the usual handicaps of governor deadband, sluggishness due to dashpot damping, and nonlinear power-gate characteristic of the turbine combine to frustrate the effort to obtain prompt and proportional load response to a given load order.

According to Curtis' earlier paper,[1] the normal band of phase deviation appears to be about $^1/_{60}$ second or 360 electrical degrees (60 cycle). To contain deviations within this narrow band would certainly require high speed response from regulating generators. If these are hydroelectric units, and if means were employed to overcome governor sluggishness, and so forth, for example by operating directly on the pilot valve, water inertia in the penstock would tend to defeat the purpose by producing in the first few seconds a change in torque opposite to that which is intended. Although the authors deal with penstock and turbine constants mathematically, it is not clear whether this characteristic effect of water inertia has been allowed for. For the same reason it seems debatable whether such high-speed control would assist or oppose stability of hydroelectric systems under fault conditions.

Figure 2 of the paper is confusing, in that time apparently runs left to right in the upper diagram but right to left in the lower one. A corresponding diagram for the Southern Ontario System of the Hydro-Electric Power Commission of Ontario is shown in Figure 1 of the discussion. Only the instantaneous frequency and integrated frequency or phase deviation are shown, and though measured on the 25-cycle system, the results are shown in terms of equivalent 60-cycle values. The interconnected area has roughly 2,000,000 kw of generating capacity. The record was taken at Toronto at 6:22 p.m. on Tuesday, August 10, 1948. The swings of frequency are due to load swings from a hot strip mill at Hamilton, 40 miles west of Toronto. The load increases in about 10 seconds to 20,000 kw peak which is maintained about 10 seconds, then drops off in 10 seconds or less. Automatic frequency control of a comparatively slow-acting type

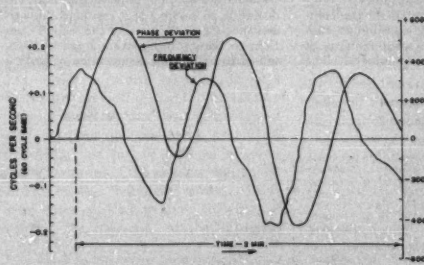

Figure 1. Frequency and phase deviations. Southern Ontario system of The Hydro-Electric Power Commission of Ontario. Swings are due to load cycle of a hot strip mill located 40 miles from the point of measurement

was in service, controlling three generators of 40,000 kw capacity each at Queenston plant, Niagara Falls. The frequency swings are not visibly changed by cutting off the frequency control.

It will be noted that phase deviations are in the order of ±400 degrees or more which means that the system is outside the normal 360-degree band most of the time. This is perhaps a bard case but it occurs normally several hours each day. The rate of load change, about 2,000 kw per second, could probably be matched by the Queenston generators without endangering the penstocks, but not through the medium of the standard governor controls with normal governor damping. Although interconnection with larger systems would reduce the magnitude of the frequency and phase-angle swings it would not appreciably reduce the duty on the local generators and regulating equipment for area control, otherwise the swings would appear on the tie-line load.

The new control system aims to reduce load fluctuations on tie lines as illustrated in Figure 1 of the paper, but a tie-line deviation still exists and seems to require a minimum droop of perhaps five per cent, for stability. To secure flat tie-line control, which is often desirable, an automatic slow-acting bias might serve. This, it would seem, would necessitate telemetering if the tie-line metering points were remote from the control stations.

The proposed new system of control would not work in conjunction with any existing load-frequency control such as that which is now in extensive successful use. A new system to displace an existing one would need to have convincing superiority not only in theory but also in actual results under practical operating conditions. Some of the practical aspects seem to require further study and tests, especially the problem of converting a load order into a load response with existing facilities.

Reference

1. See reference 2 of the paper.

H. W. Cory (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors are to be commended for undertaking the analysis of a hydroelectric system by means of transfer functions of the components of the system and the Nyquist diagram of the complete system to indicate stability.

Of particular value, and not readily apparent from the paper, is the use of the tangent circle about the $-1, 0$ point on the Nyquist diagram to indicate damping ratio or a measure of the system stability.

The analysis given depends on the computing mechanism giving continuous control. In this connection what amount of backlash is present in the θ contacts? The amount of dead band caused by these contacts would appear to make the control discontinuous if of any appreciable magnitude.

What would the difficulty be in obtaining the constants for equation 2 for a given system under varying tie line connections and generation capacity?

What rates of load pickup would be required or expected on units controlled by the system described?

Would it be feasible to use a central frequency standard with a signal distributed to all stations controlled by the system rather than depending on separate frequency standards for each station?

Has consideration been given to the stability at light load versus full load for a given setting of the computer and to the effect of nonlinearity of the various elements of the system on the calculated stability?

T. E. Curtis: Duff has raised a number of questions which merit detailed consideration, even beyond the scope of the paper or this closure. Regarding the application of the load order, the authors were thinking in terms of a low-energy synchro output signal from a centrally-located computer. Individual governors would be adopted with conventional synchro follow-up devices. Alternatively, each governor could be equipped with a direct-acting computer.

In reply to questions regarding water inertia and dashpot damping, careful study of Figures 7 and 8 of the paper should make the situation abundantly clear. Nonlinearity of power-gate characteristics were not considered in the paper in view of data in reference 8 which indicates that they may be neglected.

Duff is correct in noting the error in the plotting of Figure 2 (A and B). The corresponding diagram for the Southern Ontario System is most interesting, and it would be very desirable to interpret the data into an estimate of performance under the proposed system of control. Obviously, such an estimate would require knowledge of detailed constants of the system. Also, as indicated in the paper, solution of the equations would probably require the assistance of large scale computing devices. Such solutions would indicate whether or not the anticipated superiority of operation would merit full-scale tests.

Cory has asked five excellent questions. In reply to his first question, control is actually discontinuous by definition, but may be regarded as continuous in that the spacing of the θ contacts merely represents an error between calculated and actual ϕ. Tests of the computer indicate that it may be desirable to make the θ contacts "dither" continuously.[1] Regarding the second question, constants for equation 2 can be determined from logical consideration of particular data. It is true that resolution of data for a large interconnection may become difficult, although Figure 2 (A and B) illustrates that the actual change of constants may be very small even for major changes in the interconnection capacity.

Regarding the third question, load pickup would necessarily be determined by prime mover limitations, ranging from nearly instantaneous in some steam cases to one minute from no load to full load in hydroelectric installations. Attention is invited to the difference between gate servomotor response and load change rates dictated by penstock pressure rise. It is the gate servomotor response which must be considered in transfer function analysis of stability. Regarding the fourth question, it would be feasible to use a central frequency standard. An installation of this nature is described in reference 2. It would probably be more satisfactory and economical to use independent frequency standards at controlling power plants, since commercially-available primary frequency standards now have sufficient stability to remain substantially in synchronism with each other over long periods of time. Independence from the vagaries of communication systems would thereby be obtained.

Regarding the fifth question, the paper described the problem under what appears to be the most difficult conditions for stability. Nonlinearity of penstock performance is perhaps the most important nonlinearity present in the system, and its effect is worse at full load. At lighter loads, Figure 8 would retain the same shape, but values of ψ would shift counter-clockwise so that the lag angle would be reduced at a particular frequency. Other nonlinearities, such as power-gate and turbine efficiency characteristics, and dead band, have been neglected. The effect of dead band could be introduced into the calculations following the approach of reference 3 of this closure.

Credit for the first proposal of system-wide load division by means of accurate time control belongs to H. C. Forbes and F. Zogbaum.[3] It is believed that this paper advances the concept to preliminary quantitative terms. Much work remains to be done before the application stage can be achieved.

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1. Fundamental Theory of Servomechanisms (book), LeRoy A. MacColl. D. Van Nostrand Company, Inc., New York, 1945, pages 83–7.

2. Frequency and Time Control Aided by Telephone Company, H. C. Forbes, F. Zogbaum. *Electrical World*, volume C 111, January 20, 1934, pages 117–18.

3. Effect of Prime-Mover Speed Governor Characteristics on Power-System Frequency Variations and Tie-Line Power Swings, C. Concordia, S. B. Crary, E. E. Parker. AIEE Transactions, volume 60, 1941, pages 559–67.

Application of Low Frequency to Industrial Control Problems

N. L. SCHMITZ
ASSOCIATE AIEE

THE PURPOSE of this paper is to consider a means of operating an induction motor at slow speeds in a manner analogous to that of a d-c motor supplied from a small low-voltage generator. Motor characteristics at low voltages and low frequency are analyzed and extended to the special case of direct current. The problems involved in generating polyphase low-frequency voltages of useful magnitude are set forth and their solution by means of single-phase self-excited a-c commutator-type generators is described.

Motor Characteristics with Constant Flux Density

At normal voltage and frequency the voltage drop in an induction motor due to primary resistance and leakage reactance is relatively small, about 3 per cent and 7 per cent, respectively, of normal voltage at full load. Consequently the generated electromotive force (back emf) and flux are essentially constant in magnitude for wide changes in load. Such is not the case when we reduce the frequency and impressed voltage. Although the generated electromotive force and leakage reactance drop decrease in proportion to the frequency, the resistance drop (for any given load current) remains constant. Thus at 3 per cent frequency and at full load the resistance drop is of the same order of magnitude as the generated electromotive force. In order to maintain normal flux under this condition it is necessary to increase the impressed voltage until it exceeds the generated electromotive caused by normal flux at 3 per cent of normal frequency by an amount sufficient to compensate for the resistance and reactance drop. Assuming that flux is held constant in this manner, torque is expressed by the relation:

$$T = K_1 \phi I_2 \cos(\phi, I_2)^* \tag{1}$$

where

$$I_2 = \frac{K_2 \phi \omega_2}{\sqrt{r^2 + \omega_2^2 L_2^2}} \tag{2}$$

$$\cos(\phi, I_2) = \frac{r_2}{\sqrt{r^2 + \omega_2^2 L_2^2}} \tag{3}$$

Substituting equations 2 and 3 in equation 1

$$T = K \phi^2 \frac{\omega_2 r_2}{r^2 + \omega_2^2 L_2^2} \tag{4}$$

From the above relation we see that the torque depends only upon ω_2 for any given motor and flux. Hence, for any given motor and flux, the rpm difference between shaft speed and synchronous speed is fixed for a given torque. Thus, decreasing the frequency while maintaining constant flux shifts the speed torque characteristic *A* in Figure 1 downward, parallel to itself, to produce characteristic *B*.

Motor Characteristics With Constant Impressed Voltage

Since it is not practical to maintain the flux density constant, we must devise a means for predicting the torque characteristics under the more usual condition of constant impressed voltage. At normal frequency this can be done quite accurately by means of the ordinary equivalent circuit of the induction motor. However, when we wish to show the effect of varying the frequency of the impressed voltages, it is more convenient to use equation 30 derived in Appendix I:

$$T = 3 V_1^2 \frac{M_0^2 r_2}{[rR - \omega_1 \omega_2 (L_0 l_0 - M_0^2)]^2 + [\omega_1 r L_0 + \omega_2 R l_0]^2} \tag{30}$$

This relation can be obtained by regarding the primary and secondary windings of the induction motor as comprising two inductively coupled circuits and calculating the torque in terms of winding resistances, self inductances, and mutual inductance.

As the impressed frequency is decreased, the primary current becomes less dependent upon the load conditions of the motor. Therefore, the magnitude of the no-load current approaches that of the full-load current. This may be seen from equations 29 and 32 in Appendix I. From this we see that for an impressed voltage corresponding to normal full load current, secondary frequency, and torque, the magnetizing current at no load will exceed its normal value to an extent determined by the reduction in frequency. This is of no serious consequence because even though the magnetic circuit is to some extent saturated, hysteresis and eddy current losses are not excessive when the frequency is low. We can thus achieve a normal slip-torque relation from no load to full load torque by decreasing the primary voltages in somewhat less than a direct proportion to the frequency. As would be expected, however, for loads greater than normal, the primary current does not increase as rapidly with low frequencies as with normal frequency. Consequently, the maximum torque is less than would be obtained at normal frequency.

The maximum torque can be increased by raising the primary voltage at the expense of increased no load currents, but the final decision must be made on the

* See Appendix II for definition of symbols.

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N. L. SCHMITZ is with Cutler-Hammer, Inc., Milwaukee, Wis.

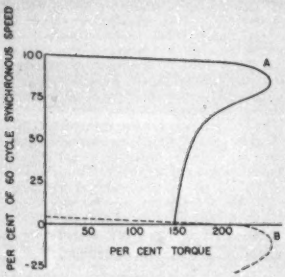


Figure 1. Speed torque characteristics of squirrel cage motor

(A) Normal voltage and frequency (60 cycles); (B) Normal flux, reduced frequency (4 cycles)

basis of duty cycle, torque requirements, and allowable speed variation.

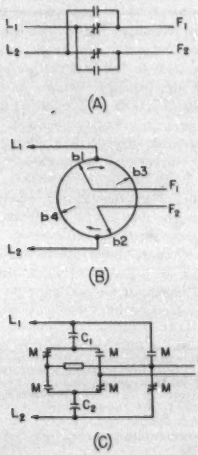
Methods of Generating Polyphase Low Frequency Power

Many schemes have been investigated. Among these are:

1. Synchronous generators running at low speed.

Figure 2. Methods of obtaining low frequency alternating voltages

(A) Reversing contactors; (B) Motor driven potentiometer; (C) Alternate discharge of capacitors (C_1 and C_2) F_1 and F_2 are low frequency output terminals; L_1 and L_2 are d-c supply lines



2. Frequency changers similar to a slip ring motor.

3. Polyphase commutator machines.

4. Single-phase commutator machines suitably interconnected to give polyphase voltages.

In methods 1 and 2, the generated voltage is directly proportional to frequency and speed. Consequently, large, slow speed machines are required for even moderate power output at frequencies of 2 to 5 cycles. In method 3, relatively small, high speed machines can be used, but as in all polyphase rotating machines, the field of flux rotates in space and consequently there is no fixed neutral axis for commutation. In some machines of this type, (such as the "ohmic drop exciter")[8] the frequency of the output voltage is proportional to the difference between synchronous speed and shaft speed. Since both these quantities are large, any slight change in shaft speed causes a large percentage change in output frequency.

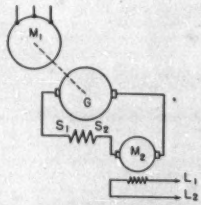


Figure 3. Self-excited low frequency generator with transformer coupling

(M) Driving motor; (G) Commutator type generator; (T) Coupling transformer; (L_1 — L_2) Power source for applying momentary d-c starting pulse

These difficulties are not insurmountable as evidenced by the practical application of a polyphase commutator machine in generating frequencies of 2.5 cycles.[2]

The advantages inherent in single-phase commutator machines are relative independence of frequency with changes in load, and the existence of a neutral commutating axis. The single-phase generator may be either self excited or separately excited. Among the methods of separate excitation are:

1. Motor driven potentiometer.

2. Reversing contactors.

3. Alternate discharge of oppositely charged capacitors.

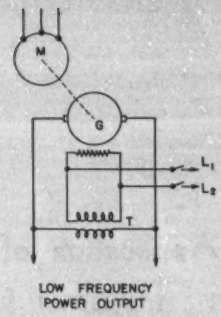


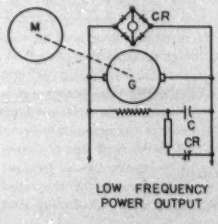
Figure 4. 'Bucking bronco' motor-generator connection

(M_1) Driving motor; (G) Series generator; (M_2) Shunt motor; (L_1 — L_2) Separate excitation for M_2

These methods are illustrated in Figure 2. The motor driven potentiometer may be so proportioned as to give a sine wave of exciting current. This feature can also be approached in method 3 by selecting capacitors which will provide series resonance with the generator field inductance at the required frequency. Even with relatively low voltage field circuits, low resonant frequencies can be obtained by using electrolytic capacitors for this purpose. Although these illustrations apply to single-phase excitation, they may also be used to excite several generators in a polyphase system. For example, in Figure 2(B) another pair of brushes b_3 and b_4 can be mounted at right angles to brushes b_1 and b_2 so as to excite the field of a second generator in a 2-phase system.

Figure 5. Low frequency generator with capacitive feedback

(M) Driving motor; (G) Commutator type generator; (CR) Control relay for self starting as a d-c shunt generator



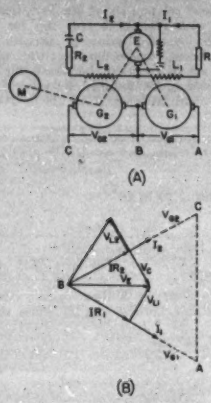


Figure 6. V-Connected separately excited generators for 3-phase power

(A) Circuit connections; (B) Vector relations

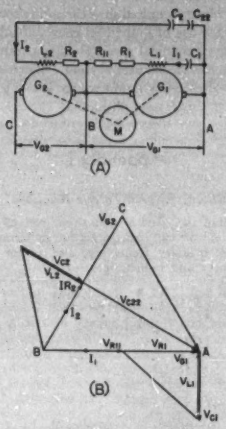


Figure 7. V-Connected self-excited generators for 3-phase power

(A) Circuit connections; (B) Vector relations

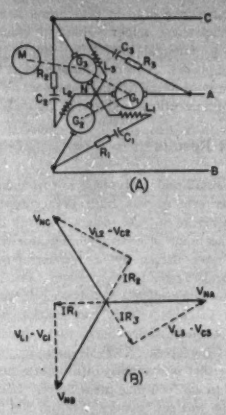


Figure 8. Y-Connected self-excited generators for 3-phase power with balanced generator impedances

(A) Circuit connections; (B) Vector relations

Self Excitation of a Single-Phase Generator

In all of the foregoing methods of excitation, the commutator machine served as an amplifier to magnify the low energy control signal and provide useful power output. If the machine can serve as an amplifier in the same manner as a 3-element vacuum tube, we might also expect it to function as a feed-back oscillator.[9] Self excitation may be accomplished through either inductive[7] or capacitive coupling.

The method of inductive coupling shown in Figure 3 has some of the features of a relaxation oscillator. An initial d-c starting current causes the generator to build up a voltage which causes an aiding electromotive force to be induced in the secondary of transformer T. This continues until the rate of voltage increase declines as a result of magnetic saturation either in the generator field or in the transformer. The exciting current decreases, causing a reduction in voltage and a reversal of the cycle. Because of the relatively large size transformer required at low frequencies, capacitive feedback has proved more economical.

Capacitive feedback for a single-phase machine was proposed as early as 1920.[10] It has also been familiar as a laboratory curiosity in the "bucking bronco" motor shown in Figure 4. In Figure 4, the capacitor C shown in Figure 5 has the form of a separately excited shunt motor with its brushes connected in place of the capacitor terminals. The generator (which is usually a d-c series machine) is driven at a constant speed and causes the shunt motor to accelerate until its back electromotive force exceeds that of the generator, whereupon the series field excitation reverses, in effect "plugging" the shunt motor until it reverses rotation and begins the opposite half cycle.

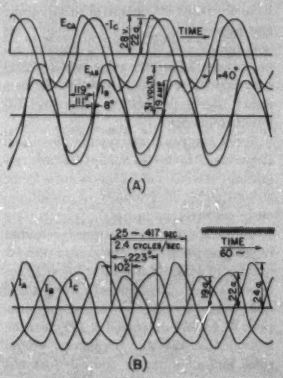
By employing a shunt generator and a static capacitor in the manner outlined before we obtain a constant potential, low frequency source of single-phase power. Connections are shown in Figure 5. The efficiency of the generator may be increased by laminating its field structure to reduce eddy current losses. Voltage regulation and commutation are improved by adding compensating windings to neutralize armature reactance and prevent field distortion. The system can be made self starting by using the CR relay, shown in Figure 5, to shunt the capacitor with a low resistance whenever the generator voltage is less than 70 per cent. This prevents demagnetizing the field when the generator is shut down and allows the machine to build up as a d-c shunt generator when starting. With these improvements, a practical source of low-frequency power is obtained.

Polyphase Power from Single-Phase Machines

Polyphase power may be obtained by employing two single-phase generators having their field currents displaced 60 degrees. When a "V" connection is employed as shown in Figure 6(A) a 3-phase system is obtained. With this connection the generator field circuits have conjugate impedanced, as shown in the vector diagram Figure 6(B). We may dispense with exciter E if we use one of the main generators to supply both field circuits as shown in Figure 7.

Figure 9. Instantaneous currents and voltages with generators connected as shown in Figure 6

(A) Generator voltages and currents; (B) Load currents



Impedance drop in generators G_1 and G_2 may cause minor voltage unbalance due to unequal impedance among the three phases. This can be avoided at the expense of an additional generator G_3 by using connections as shown in Figure 8.

Test Results

A small scale polyphase low-frequency power system was built in accordance with Figure 6. The generators were two 1/5-horsepower 32-volt commutator-type machines driven by a 1/2-horsepower squirrel cage induction motor. The exciter was a similar 110-volt commutator-type machine. This combination was used to successfully "inch" and "thread" four 1-horsepower 220-volt induction motors either individually or simultaneously in a model printing press drive. Static torque was set between 150 and 200 per cent of rated full load torque for each motor. Figure 9 shows the instantaneous currents and voltages obtained under full load conditions.

Summary

The performance of an induction motor on low-frequency voltages is readily predictable by means of the relations shown in Appendix I. We find from these relations that full torque is available from the motor providing the impressed frequency is at least equal to the slip frequency at which this torque would normally occur. The change in speed from full load to no load under the foregoing conditions is approximately the same as under normal operating conditions.

Small high-speed machines can be effectively employed in generating the voltages needed for low-frequency operation. Although the generating system which we have described is best suited to supplying one frequency, a selection of frequencies and speeds can be obtained by simultaneously switching the relatively low current exciting circuits to provide the necessary change in circuit constants for different frequencies.

Speed control in the manner described is particularly useful in applications requiring several induction motors to be started either individually or simultaneously without immediately accelerating to a high speed. This feature is required in printing press and other drives where a number of individual units are flexibly interconnected. The ease with which these units can be arranged in various combinations is greatly enhanced if they can be individually driven. Ordinary wound rotor induction motor control does not provide the required slow threading speeds and the use of individual gear shifting or other mechanical speed changers complicates the mechanical system. The use of a low-frequency power source is one solution to this type of problem.

Appendix I

Derivation of Speed-Torque Relations.

Using the general equations derived by Stanley[1] we can express torque in terms of speed, primary frequency, and motor resistances and inductances.

$$T_1 = -\frac{3}{2} I_\alpha M_0 i_\beta + \frac{3}{2} I_\beta M_0 i_\alpha \tag{20}$$

$$I_\alpha = \frac{V_1}{Z_1} + \frac{V_2}{Z_2} \tag{21a}$$

$$I_\beta = \frac{-jV_1}{Z_1} + \frac{jV_2}{Z_2} \tag{21b}$$

$$i_\alpha = \frac{(p-jn)M_0}{r+(p-jn)l_0}\frac{V_1}{Z_1} - \frac{(p+jn)M_0}{r+(p+jn)l_0}\frac{V_2}{Z_2} \tag{21c}$$

$$i_\beta = j\frac{(p-jn)M_0}{r+(p-jn)l_0}\frac{V_1}{Z_1} - j\frac{(p+jn)M_0}{r+(p+jn)l_0}\frac{V_2}{Z_2} \tag{21d}$$

$$Z_1 = R + pL_0 - \frac{p(p-jn)M_0^2}{r+(p-jn)l_1} \tag{21e}$$

$$Z_2 = R + pL_0 - \frac{p(p+jn)M_0^2}{r+(p+jn)l_0} \tag{21f}$$

The foregoing equations are for instantaneous, torque and current quantities, and the impedances shown are transient impedances. If we express V_1 and V_2 as vector quantities and replace the differential operator p by $j\omega_1$ we obtain the corresponding steady state relations. Equation 20 yields the average torque if we perform the multiplications $I_\alpha i_\beta$ and $I_\beta i_\alpha$, using the product of their rms values and the cosine of their phase difference. Thus equation 20 becomes

$$T = \frac{3}{2} M_0 [-I_\alpha i_\beta \cos(I_\alpha, i_\beta) + I_\beta i_\alpha \cos(I_\beta, i_\alpha)] \tag{22}$$

In the case of balanced primary voltages, $V_2 = 0$, and the quantity, $p - jn = j\omega_2$, is the rotor frequency.

With the above simplifications we may substitute equation 21a in equations 21b, 21c, and 21d to obtain

$$I_\beta = I_\alpha \angle -90 \text{ degrees} \tag{24}$$

$$i_\alpha = I_\alpha \frac{\omega_2 M_0}{\sqrt{r^2+\omega_2^2 l_0^2}} \angle 90 - \tan^{-1}\frac{\omega_2 l_0}{r} \text{ degrees} \tag{25}$$

$$i_\beta = I_\alpha \frac{\omega_2 M_0}{\sqrt{r^2+\omega_2^2 l_0^2}} \angle -\tan^{-1}\frac{\omega_2 l_0}{r} \text{ degrees} \tag{26}$$

Since

$$\cos\left(-\tan^{-1}\frac{\omega_2 l_0}{r}\right) = \frac{r}{\sqrt{r^2+\omega_2^2 l_0^2}} \tag{27}$$

We may substitute equations 24 to 27 in equation 22 to obtain

$$T = 3 I_\alpha^2 \frac{M_0^2 r \omega_2}{r^2+\omega_2^2 l_0^2} \tag{28}$$

The above relation shows the variation of torque with respect to rpm slip for the case of balanced primary currents. A corresponding expression for the more usual case of constant, balanced primary voltages may be obtained by solving for I_α by substituting equation 21e in 21a.

$$I_\alpha = V_1 \frac{r + j\omega_2 L_0}{(rR - \omega_1\omega_2 L_0 l_0 + \omega_1\omega_2 M_0^2) + j(\omega_1 r L_0 + \omega_2 R l_0)} \tag{29}$$

Substituting 29 in 28

$$T = 3 V_1^2 \frac{M^{02} r \omega_2}{[rR - \omega_1\omega_2(L_0 l_0 - M_0^2)]^2 + [\omega_1 r L_0 + \omega_2 R l_0]^2} \tag{30}$$

For the special case of direct voltage applied to the stator, both 28 and 30 reduce to

$$T = 3 I_\alpha^2 \frac{M_0^2 r \omega_2}{r^2+\omega_2^2 l_0^2} \tag{31}$$

and 29 reduces to

$$I_\alpha = V_1 \frac{r + j\omega_2 l_0}{(rR + j\omega_2 R L_0)} = \frac{V_1}{R} \tag{32}$$

Appendix II. Symbols

ϕ = airgap flux (mutual flux)
T = average three-phase torque in synchronous watts (based upon normal frequency)
T' = instantaneous torque
I_2 = secondary current
ω_1 = primary frequency in radians per second
ω_2 = secondary frequency in radians per second
n = rotor speed in electrical radians per second
R = stator resistance per phase
r = rotor resistance per phase
L_0 = apparent stator three-phase self inductance per phase
l_0 = apparent rotor three-phase self inductance per phase
M_0 = apparent three-phase mutual inductance per phase
$L_1 = L_0 - M_0$, apparent leakage inductance of stator winding per phase
$L_2 = l_0 - M_0$ apparent leakage inductance of rotor winding per phase
$p = \frac{d}{dt}$, time derivative operator
I_α = stator current per phase in positive α axis
I_β = stator current per phase in positive β axis
E_α = stator terminal voltage per phase in positive α axis
E_β = stator terminal voltage per phase in positive β axis
i_β = rotor current per phase in positive β axis
i_α = rotor current per phase in positive α axis
$V_1 = \frac{E_\alpha + jE_\beta}{2}$ positive-phase sequence stator voltage
$V_2 = \frac{E_\alpha - jE_\beta}{2}$ negative-phase sequence voltage
Z_1 = impedance to positive sequence currents
Z_2 = impedance to negative sequence currents

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Discussion

John F. H. Douglas (Marquette University, Milwaukee, Wis.): Low frequency has been used since 1910 to produce stroboscopic flicker, since 1915 for electric hammers, since 1925 for starting stabilizing gyroscopes, and now for inching and threading. I have been fortunate to be in on some of these tests, and wish to record that it now appears to be a must for teachers.

Concerning the induction motor behavior my tests confirm those of Schmitz. However it should be mentioned that with low frequency and high stator IR drop, saturations become rather high at light load.

Concerning methods of generation of low frequency. Doctor Bennet should discuss the oscillating machine, and some General Electric man should state the capabilities of the brush-shifting 3-phase commutator machine as a frequency changer.

The author's method of analysis, although powerful, using the primitive machine axes, and differential operator, does not adequately cover nonlinearity, and the oscillograms show distorted wave form. Any self-excited machine must of necessity depend on saturation for no-load voltage stability.

Some remarks on how to demonstrate low frequency effects in the college laboratory may be in order. Almost any laboratory can demonstrate them with existing equipment.

If 15 cycles are desired laminated yokes and poles are necessary, but compensated machines are not necessary. If 5 cycles are sufficient, laminated poles and solid yokes can be used, but with solid poles and yokes one cycle is about the limit.

When eddy currents are present Schmitz' paper must be modified somewhat. First, the d-c saturation curve is higher than the a-c curve. Second, the effective resistance of the shunt field is so high that the field must in general be multipled. Third, armature voltage and field current are not in phase, owing to the angle of hysteretic and eddy current advance.

The shunt commutator machine will run as an adjustable speed motor. Thus there are many problems ideally suited to the college laboratory.

Jerome M. Frank (Hevi Duty Electric Company, Milwaukee, Wis.): An interesting comparison of equation 31, concerning the torque developed when a direct voltage is applied to the stator, was given in a paper presented in December 1943 by Alfred Oklund before the Milwaukee section of the AIEE on Direct Current Breaking of Induction Motors. Oklund's analysis and correlating test data have proved very valuable to various manufacturers in the Milwaukee area, who are interested in the calculation of the torque obtainable at various speeds when a direct current is applied to the stator.

Oklund first developed the relationship between the stator and rotor magnetomotive forces, the winding constants and the direct current to be applied, by the vector addition of the first harmonic magnetomotive forces of all the active stator coils. From this he established the amplitude of the flux density in the stator and rotor. The rotor was calculated in terms of the stator so that the constants could readily be obtained from tests. He then related the direct current to its equivalent alternating current in terms of the rotor and stator flux density. From the equivalent circuit of an induction machine, it was then possible to relate the equivalent alternating current to the measured constants of the machine. For a given direct current and a constant magnetizing reactance, saturation neglected, the voltage impressed on the rotor was shown to be constant and the rotor current could then be calculated. By substituting in the basic torque formula for the rotor current, the value of rotor current obtained before, he derived an equation showing that the torque was directly proportional to the number of phases, the square of the stator current, the square of the magnetizing reactance and the rotor resistance and inversely proportional to the sum of the rotor resistance squared and the square of the sum of the magnetizing reactance and the rotor reactance. This equation is similar to that derived by Schmitz.

Oklund's equation has an advantage in that the calculations are very easy to perform as the equation is in terms of the motor constants, which are readily available from a design or a test sheet.

It was very unfortunate that the untimely death of Alfred Oklund prevented his presentation of this masterpiece of engineering analysis before a general meeting of the Institute. The writer has arranged with Dr. John F. H. Douglas, of Marquette University, Milwaukee, Wis., to furnish mimeographed copies of Oklund's paper to those interested in it.

N. L. Schmitz: The writer concurs with Douglas as to the need for a consideration of low frequencies in teaching electrical machinery courses. By starting with a simple, inductively coupled equivalent circuit and using the appropriate self and mutual inductances, general expressions can be obtained for various currents and torque in most a-c machines. Since they are not limited to any one frequency, the relations thus obtained can be used to show behavior on any desired frequency, including the special case of direct current. When this is done for an induction motor, the results are the same as obtained by the somewhat more rigorous method of Appendix I. If the $j\omega$ operator is used in place of the differential operator in writing the circuit equations, the analysis can be carried out even though the student does not have a background in differential equations.

As pointed out by Douglas, the vector diagrams shown in the paper are based on the assumption that hysteresis and eddy currents in the generator field windings are negligible in relation to the magnetizing current.

Such is usually the case with high-grade well-laminated core materials. In the event that these currents are appreciable, a minor adjustment of the circuit constants suffices to shift the total field currents to a new phase position that their magnetizing components occupy the position now occupied by the total currents.

When the a-c shunt-connected commutator machine runs either as a motor or as a generator, the voltage induced in the armature coils contains a lower and a higher frequency component. These frequency components are due to the two components of the single-phase field rotating, respectively, with and against the armature. This imposes limitations similar to those of a-c series motors regarding iron losses and induced voltages in the armature.

I consider myself fortunate to have heard Oklund present his inspiring analysis of d-c braking. His work has done much to improve the concepts of induction motor behavior on both direct and low frequency currents.

Dynamic Lowering D-C Hoist Control

D. L. PETTIT
ASSOCIATE AIEE

THIS PAPER is intended to cover a specific scheme of dynamic lowering hoist control applicable to a series type motor. It is not intended to cover the study of performance of a d-c series wound motor, but a short review may be appropriate. Figure 1 shows a typical characteristic curve for a series wound motor. Of particular importance is the fact that such a motor tends to a runaway condition as torque is removed from its shaft. The dynamic lowering circuit circumvents this condition by separately exciting the series field so that the motor functions more nearly as a shunt motor when the speed is reasonably stable with variations in load.

The application of dynamic lowering is not new. Various circuits are in existence to accomplish this function. The details of such circuits are usually quite complex and vary considerably in accordance with the individual originating them. In order to obtain the required features, more contactors are required than in the conventional reversing circuit, more electrical interlocking is necessary and the application of mechanical interlocking is quite limited. The circuit described in this paper makes use of a minimum number of contactors and produces extra safety features not heretofore offered in dynamic lowering design.

Basic Considerations

A series motor is normally used in order that high torque may be developed to lift heavy loads. For hoisting a slack cable a point is introduced by using a shunting resistor across the field and armature to obtain a low-torque low-speed characteristic. This may also be used to obtain low-speed light-hook operation.

The shunting resistor may then be removed, possibly in steps, to produce increases in speed in the hoisting direction. Series resistors are also short circuited in order to reach the full-speed full-load condition.

For dynamic lowering, the field is excited by current drawn from the line through a current-limiting resistor, while the armature is connected to low-voltage points obtained by a potentiometer method, using the field circuit for the main path of the potentiometer.

Such an arrangement makes it possible to stabilize the speed in lowering, so that when the heavy loads deliver power back through the gear mechanism to the motor, this power is dissipated by conversion to electric power and dissipated in the field and resistor circuit. At the higher speed, part of the power may be returned to the line.

In order to assure adequate torque at the low speed, a strong field is provided. This current, usually in excess of full load current, is permitted to pass through the series brake so that positive release of the brake is obtained.

The lowering speed is increased in three ways. Additional resistance may be inserted in the armature circuit, permitting the armature to operate at a higher voltage and consequently higher speed. The armature may be connected across points in the potentiometer circuit which are at a higher potential, thereby increasing the voltage across the armature and, in turn, the speed of the armature. The field strength may be weakened, thereby requiring a higher speed of the armature in order to produce the proper counter electromotive force. This latter is particularly necessary in order to obtain high-speed light-hook lowering condition, wherein the usual requirement is to obtain at least 200 per cent of full load speed for lowering. Herein lies one of the principal advantages of the dynamic lowering system since it speeds up operation of the hoist thereby increasing its productive effort, and at the same time eliminating the attending maintenance problems that are involved in the application of load brakes.

The series brake is conventionally employed as it offers the greatest safety. Since it is spring set, it introduces safe failure in the case of voltage or current failures. It is, of course, necessary to pass sufficient current through the brake on the first point hoisting and the first point lowering in order to assure prompt and positive release of the brake. On subsequent higher speed points, it is only necessary to maintain adequate current through the brake coil in order to hold it in the released position. The series brake gives a safe condition in the event of a voltage failure which might be introduced by the tripping of an overload device, the opening of a fuse or circuit breaker, or failure within the power distribution system itself.

In order to enhance the control which the operator has over the hook load, the first few speed points on the master should involve smaller changes in speed than those near the high-speed positions. Since heavy loads on the hook have a tendency to spread out the low-speed points in lowering, and light-hook conditions spread out the low-speed points in hoisting, designs which incorporate lower speed increments at lower speed permit operators to handle light and heavy loads with almost equal dexterity.

When the master is moved from point to point, current peaks should be held to a minimum in order that the life of the controller and the life of the motor commutator may be prolonged.

It is advisable to incorporate automatic acceleration so that operation of the master switch handle too rapidly will not produce undesirable results. These time delays are considered necessary in connection with acceleration of the motor,

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D. L. PETTIT is with the industrial controller division of the Square D Company, Milwaukee, Wis.

but not in connection with deceleration. Of particular importance is this point with regard to decelerating the load when lowering. In this condition, the motor must not only absorb the energy which the load is delivering back through the gear mechanism to the motor under a steady-state condition, but it must also produce the necessary torque to decelerate the load when the operator desires to slow down the lowering speed. No time delays are to be provided in this decelerating action as such time delays might take control away from the operator just when it is needed most. The operator must retain control of this deceleration even though it becomes necessary to decelerate at a rate which overtaxes the motor or the hoist mechanism. Such overtaxing may be preferable to damage which might result if the load could not be brought to a stop quickly.

Analysis of Functions

The circuit under consideration employs nine contactors and six resistor sections excluding the hoist limit stop. Figures 2 and 3 show the load and resistor circuit connections employed for each speed point. Since the dynamic lowering requirements govern the circuit design, its study will be first.

A field current between 100 and 200 per cent is obtained by a series parallel connection on a first speed lowering. The actual current in the field circuit is dependent upon the hook load. The armature is connected across the field itself through two parallel circuits, each of which consists of a resistor section and a spring-closed contactor. This circuit imposes a very low voltage across the armature. This low voltage when combined with the strong field established produces a low speed for the first lowering condition. Notice that the series brake carries all the line current in order to insure prompt and positive lifting of the brake.

Smooth action on the first lowering speed is obtained by appropriate choice of the ohmic value of the resistors in series with the armature. Since the time constant of the field prevents the field strength from being established instantly, the momentary torque of the motor in the downward direction is limited by these resistors to such a magnitude that transient overspeeding before the steady-state condition is reached is prevented.

Speed increases in the downward direction are obtained by a unique circuit, making use of a bridge resistor system, controlled by two spring closed contactors. Extra resistance is inserted in the armature circuit for the second lowering speed by removing one of the shunt paths in series with the armature. This permits the load to accelerate the armature to a higher speed.

A bridge circuit is established in the third speed increasing the voltage across the armature which, in turn, increases the lowering speed. The remaining spring closed contactor is opened to obtain the fourth lowering speed, leaving the armature connected to the resistor network in such a way as to further increase the voltage in the armature circuit which, in turn, further increases the lowering speed. Transfers through these speeds is attended with a closed loop at all times, containing the armature, the field, and some resistors so that regenerative power can be dissipated even though voltage failures may be encountered.

The armature is then transferred to line voltage to obtain speed 5. This is accomplished by closing contactor *2M* after the opening of contactor *2A*. It will be noted that the reverse order of operation of these two contactors would produce considerably higher current peaks since the reverse order would involve both of them being closed at the same time, thereby short circuiting a considerable portion of resistor network and would draw heavier current for the field momentarily as well as the armature. The opening of contactor *2A* first permits a resistance transfer minimizing the current peaks and thereby enhancing both commutator life and contactor life. In the steady-state condition for the fifth speed, the armature is essentially at line voltage and the field is excited at slightly less than the full load current. Only resistor 4 is in the armature circuit. This resistor serves to limit the current peaks as the transitions are made.

Pneumatic timers are employed in the transfers between speeds 4 and 5, and 5 and 6. Notice that the timing is so arranged that the transfer from speed 4 to 5 can be made immediately if operation has been maintained in speed 4 sufficiently long for the required timing period to have elapsed.

Speed 6 employs a weak field and is obtained by inserting resistance in the field circuit. Automatic control is imposed in the form of a current relay and a time delay. Relay *LR* closes its contact if the armature current exceeds a certain value. If its contacts are closed, contactor *1M* cannot be opened. By setting relay *LR* to function at between one quarter and one third of full load current, the operator cannot transfer to speed 6 unless the hook is lightly loaded. The time delay is imposed so that transient condition will have subsided in speed 5 before relay *LR* is permitted to open contactor *2M* for speed 6. If the master is

Figure 1. Typical characteristic curve for a series wound motor

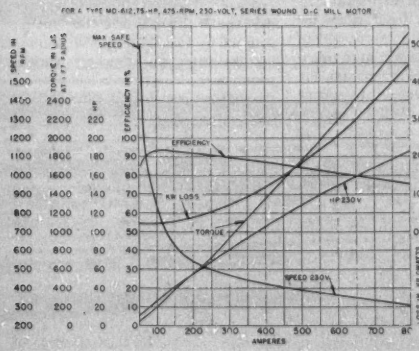
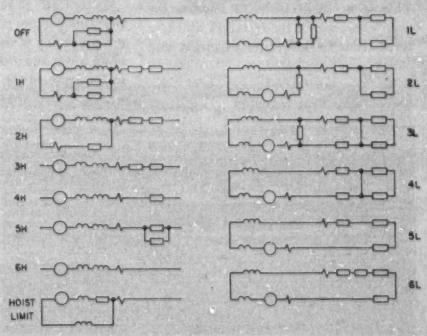
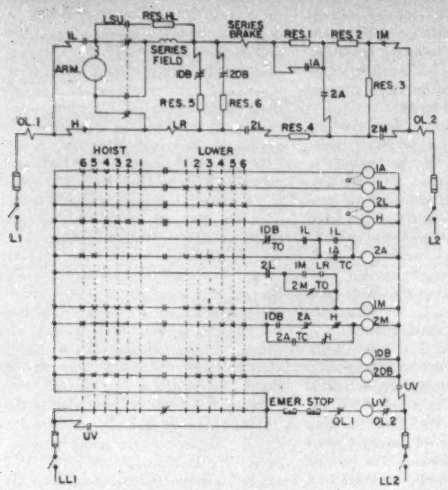


Figure 2. Step by step load circuits





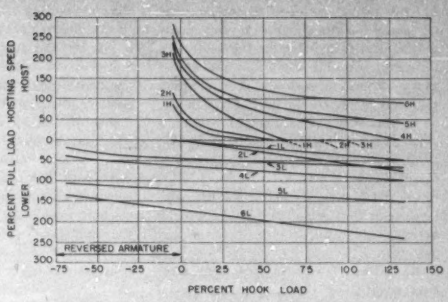


Figure 3(a) (left). Composite load and control

Figure 4 (above). Typical speed curves

moved quickly all the way into the full-speed lowering condition, this timer serves to provide automatic acceleration. Notice that the circuit is so arranged that once the contacts of *LR* have opened to insert the extra resistance 3, transient reclosing of *LR* has no further effect on the circuit.

In returning to lower speeds, speed 5 is obtained by increasing the field strength reducing the speed of the armature in much the same manner as is obtained with a shunt motor when the field is increased.

Speed 4 introduces the bridge circuit again. Contactor *2A* closes first as the speed is reduced, immediately followed by the opening of *2M*. The field is thus first strengthened while contactors *2A* and *2M* are closed at the same time, leaving only resistance 1 to limit the field current. This strong field tends to increase the counter voltage of the armature which, in turn, tends to deliver more current into the resistor network so that when contactor *2M* opens, a reduction in current has already started relieving the load which the contactor must open.

Speed 3 is obtained by permitting one of the spring closed contactors to close, reducing the voltage across the armature and thereby lowering the speed. Notice that since only one of these spring-closed contactors closes in this condition, the higher resistance value serves as a peak limiter.

Speed 2 is obtained by opening contactor *2L* leaving the armature connected through one resistor directly across the field. A further reduction in speed is obtained by permitting the remaining spring-closed contactor to close its contact, reducing the resistance in the armature circuit by the extra parallel path.

In the "off" position, the armature, the field, and the two parallel resistor sections are connected in such a way as to regenerate power and produce dynamic braking in the event of the failure of the voltage source or the spring set brake. Such an arrangement limits the lowering speed in the event of voltage and brake failure so that runaway speed and attending damage to personnel, equipment, and property is avoided.

	HOIST							LOWER					
	6	5	4	3	2	1	°F	1	2	3	4	5	6
1A	X	X	X										
1L								X	X	X	X	X	X
2A	X	X						X	X	X	X		
2L										X	X	X	X
1M	X	X	X	X	X	X		X	X	X	X	X	
2M	X											X	X
H	X	X	X	X	X	X							
IDB						X	X	X	X	X			
2DB					X	X	X	X					

Figure 3(b). Contactor sequence

Salient Features

Two spring-closed contactors employed in this manner produce particularly desirable results. Speed transfers are obtained with current peaks held to reasonable limits with the attending reduction in maintenance both on the controller and the motor itself. The connection of these contactors in parallel paths permits the use of contactors one size smaller than are employed in the remainder of the circuit since the currents in the regeneration circuit are usually of the order one-half of the full load current of the motor. Two contactors arranged in this fashion give extra safety. Since it is important that regenerative circuit be maintained under all circumstances to insure safe failure, the connection of these two contactors in this manner can be more fully appreciated when it is considered that the parallel contactor and resistor circuit so arranged are much more remote in the possibility of failure than one contactor and one resistor circuit in comparison.

Consideration of motor instability are quite important in dynamic lowering. A weak field must be guarded against when heavy loads are involved as instability may introduce a runaway condition and its attending damage. Relay *LR* is used to prevent such conditions. Test work in connection with this control and a ball-bearing test rig indicate that speeds in excess of 200 per cent of full load might be developed with a weak field when lowering with full load on the hook without passing the point of instability. Varying performance characteristics found among the different motors and different hoist mechanisms, particularly with regard to the efficiency of the hoist mechanism, make it necessary to be cautious in the approach to the point of instability. A safe consideration is to employ a field strength approximately that of full load as a minimum field for heavy hook conditions.

It will be noted that the brake is connected in the circuit so that it is required to carry all the line current on the first point lowering, but on subsequent points it carries only the field current. Such an

	RES 1	RES 2	RES 3	RES 4	RES 5	RES 6
6H	○	○	○	○	○	○
5H	○	−	−	○	○	○
4H	○	−	○	○	○	○
3H	−	−	○	○	○	○
2H	−	−	○	○	○	+
1H	−	−	○	○	+	+
OFF	○	○	○	○	+	+
1L	+	+	+	○	+	+
2L	+	+	+	○	+	○
3L	+	+	+	−	+	○
4L	+	+	+	+	○	○
5L	+	+	○	+	○	○
6L	+	+	+	+	○	○

+ SPEED INCREASES AS RESISTANCE INCREASES
− SPEED DECREASES AS RESISTANCE INCREASES
○ SPEED UNCHANGED AS RESISTANCE INCREASES

Figure 5. Effects of changing resistor values

arrangement prevents overheating of the brake coil, but yet assures positive releasing of the brake on the first point.

Similarly, the brake is required to carry the shunt current as well as the motor current on the first point hoisting to insure prompt and positive lifting of the brake shoe. However, if the motor is permitted to operate under a light-hook full-speed hoisting condition, the current may subside to a low value. If the brake is not properly chosen, it may reset at this low value and cause annoying reduction in speed in the operation of the hoist. Mill and plant engineers should particularly be cautious to select brakes in accordance with the motor data and thereby eliminate this type of difficulty.

The same set of resistors are used to obtain the hoisting speeds. The shunt circuit across the armature and field produces low-voltage conditions giving a low-speed point for taking up the slack in the cable.

In speed 2 one of the parallel paths in the shunt is removed to give an increase in light hook speed for manipulating light loads. In speed 3 a simple series connection is developed with a resistance for torque and speed limitation.

Speeds 4, 5, and 6 are obtained by reducing the amount of resistance in series with the motor by shorting and parallel arrangement. The paralleling of paths is accomplished for speed 5 by paralleling resistors 2 and 3. In speed 6, the motor and its series field are directly connected across the line.

Typical speed curves are shown in Figure 4. These speed curves were obtained by actual test data using a 45 horsepower, Westinghouse, type *K* motor in a test rig involving a 10-ton load. In examining these speed curves, notice that at least two low speeds are obtained for light hook manipulation in hoist direction and that at least three speeds below 100 per cent are involved for light hook lowering. More effective use is made of the speed points as the load is increased so that at 100 per cent load, at least three operating speeds are useful in the hoist direction.

Lowering speed 2 is actually a transfer speed. At light loads, this speed may be approximately equal to the first speed lowering while at extremely heavy loads it may approach that of number 3 lower. For intermediate values of load it gives intermediate speed between 1 lower and 3 lower, making it possible to obtain four useful operating speeds for heavy hook conditions, each of which are below 100 per cent. For heavy loads, a high speed in excess of 100 per cent is obtained in speed 5, while the sixth speed is locked out and is useful for light hook only.

The effects of manipulating the resistor values is shown in Figure 5. Resistor changes in the field when using a network system are minimized, as the bridging system produces better control of the hoist speed without regard to the motor characteristics. Resistors 5 and 6 may be unbalanced to secure differing responses for the second hoist and second lowering speed. The ohmic value of the motor armature and commutating field may require slight changes in resistor 4 in order to produce the proper slope to the lowering speed curves. The weak field high-speed light-hook condition is controlled by resistor 3. Since the saturation curve of the motor determines the strength of the field necessary to produce this high-speed condition, some adjustment of resistor 3 may be necessary in order to produce optimum performance of the hoist and motor combination. The usual cautions are necessary in connection with maintaining secure terminals and tight connections to the resistor banks. Extra resistance due to poor connections or loose terminals may considerably alter the performance of the motor and hoist combination.

Interpretation of the speed curves for the test setup would not be complete without a reminder that the efficiency of the hoist mechanism has the effect of moving the point on which the motor is considered to operate on these speed curves. In the test data the armature current for full-load lowering was slightly in excess of one-half that for hoisting full load. If the hoist mechanism were more efficient, the lowering current in the armature would be more and would have the effect of moving out farther on the lowering curves, producing slightly greater speed. If, on the other hand, the mechanism were less efficient and the armature current were reduced for similar load values, the operating point would

Figure 6. Oscillograms for hoisting number 326

1 Field current 20A/MM
2 Line current 20A/MM
3 Armature current 20A/MM
5 Line volts 10V/MM
6 Timing wave 60 Cycles

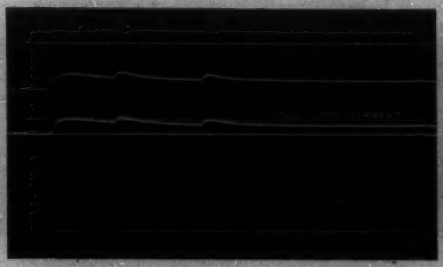
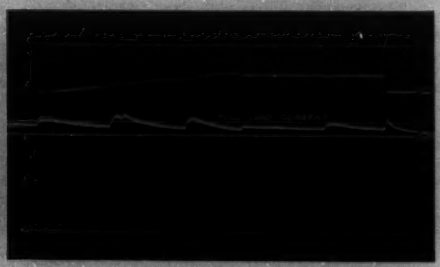


Figure 7. Oscillograms for deceleration when lowering number 329

1 Field current 20A/MM
2 Line current 20A/MM
3 Armature current 20A/MM
5 Line volts 10V/MM
6 Timing wave 60 Cycles



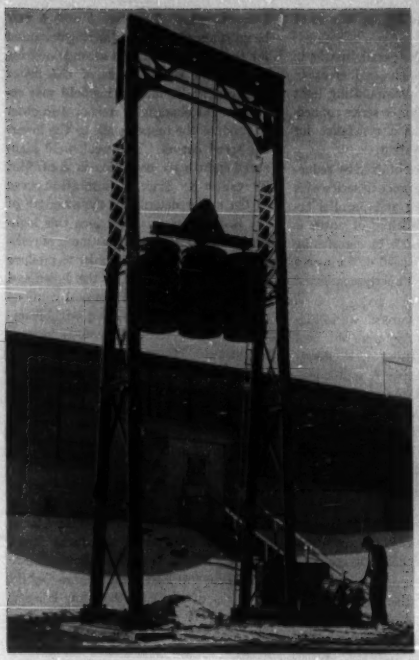


Figure 8. Test hoist tower with 10-ton load

Figure 9. Controller panel installation for testing circuit

be moved to the left on the speed curve. Since exact performance cannot be predicted accurately, hoist users must expect that the speeds obtained may vary somewhat from those indicated in this discussion. Variations in speed will also be governed by the amount of overloading or underloading of the motor with regard to the hoist to which it is applied.

Contactors *1M* and *2M* relieve each other in their duty cycle so that neither is permitted to heat excessively. The amount of relief each gives the other is dependent upon the operation of the hoist in its various speeds.

The hoist limit switch is indicated as the conventional load type of limit with a self-contained resistor. A discussion of this unit is not included in this paper.

Drifting downward may be encountered in the low-speed hoisting when heavy loads are being manipulated. Operators customarily pass through the low-speed points quickly when handling the heavier loads. The drifting condition may prove quite useful when spotting heavy loads as it gives a means of lowering at extremely low speeds.

No time delays are incorporated for decelerating the load either for hoisting or lowering. For hoisting such time delays would serve no useful purpose as lower speeds are obtained by inserting resistance in the circuit, but for lowering, time delays may or may not be useful. Such delays are considered the exception rather than the rule as the operator must have complete control of the load in order to prevent accidents, damage, or injury under unforeseen circumstances. It is considered preferable to overtax the motor and the controller with heavy current peaks rather than risk danger to life or property.

Oscillograms shown in Figures 6 and 7 indicate current peaks for hoisting and lowering. Decelerating curves were obtained by rapid motion of the handle from full-speed lowering to the "off" position. Low commutating peaks are indicated as well as low values of current which the contactors must make and break.

Conclusion

The dynamic lowering scheme presented herein involves the use of a minimum number of nine contactors to obtain a six-speed system. Low commutating peaks within the controller minimize maintenance in connection with the contactors. Low accelerating peaks of current minimize maintenance of motor brushes and commutator. Added safety in operation is obtained by the use of two paths for regenerative current, greatly reducing the possibility of contactor, resistor and circuit failure and resultant runaway condition. Maximum re-use of resistor sections is practiced throughout the operation of the controller, minimizing the physical space requirements for the resistor. By reusing resistor sections, their design approaches that of continuously rated, thereby giving them a greater operating reserve in capacity.

Discussion

J. W. Cooke and G. W. Heumann (General Electric Company, Schenectady, N. Y.): The author has presented an interesting paper on a crane hoist controller, the outstanding feature of which is the efficient utilization of contactors and resistor sections for obtaining required hoisting and lowering operating characteristics. The controller described contains many desirable features. However, there are several points on which we should like to comment.

One of the fundamental principles to be considered in the design of a dynamic braking hoist controller is the maintaining of a field-armature loop circuit under all operating conditions lowering. Control equipment is subject to wear and maintenance, and even with the best design and manufacture such failures as breakage of a control wire, failure of a master switch contact, burnout of a contactor coil and so forth have to be considered as possible causes of incorrect controller performance.

In Figure 3 of the paper, dynamic braking contactors *1DB* and *2DB* are under direct control of the master switch contacts without any apparent interlocking with other contactors. When the master switch is notched to point 4 lower or beyond, contactors *1DB* and *2DB* are open. The loop around the motor armature is thereby removed, regardless of whether or not the other contactors have operated in proper sequence. For example, if contactor *2L* has not closed on account of a coil burnout, a wire breakage, or the like, the motor armature circuit would be open with the series brake released, resulting in loss of dynamic braking torque and overspeeding of an overhauling load.

The sixth point lower provides for a weak field connection for lowering light loads. A current relay *LR* automatically maintains a stronger field on this point if the load exceeds a certain predetermined amount. It is noted that relay *LR* operates in conjunction with normally open contactor *1M*. Should this contactor fail to close, heavy loads would be lowered under a weak field condition and excessive speeds would result. It appears to be preferable to use a spring-closed contactor for weakening the field for high-speed light-load operation.

Figure 2 of the paper indicates that on points 5 and 6 lower the motor is connected as a straight shunt motor with the field and armature circuits with their respective resistor sections connected straight across the constant-voltage line. In a paper which had been presented previously before the Institute[1] it has been pointed out that such a connection results in a lower stability limit than is obtained by the addition of a block of resistance in series with the line.

The speed-torque curves given in Figure 4 of the paper showed straight lines for points *5L* and *6L*. As the torque is expressed in per cent hook load, a full evaluation of these curves would require a knowledge of the loading of the motor with respect to its rating. The author mentions tests made with a 45-horsepower motor on a 10-ton hoist, but as no information is given on the speed of the load, it is not possible to evaluate what the load values represent in terms of motor torque. The straight-line curves for points *5L* and *6L* do not show any approach of a stability limit, and it would be interesting to know where the stability limit is to be expected.

In Figure 1 of the paper are shown the characteristic curves of a standard *MD612* series motor. This motor is rated 75 horsepower, 60 minutes. On an average crane installation this motor would be applied on the basis of its 100 horsepower, 30 minutes ability. Speed-torque curves have been calculated for this motor, using the formulas developed in the previously cited paper,[1]

$$T = KI_A \qquad S = \frac{E}{K}$$

where

T = per unit torque
I_A = per unit armature current
S = per unit speed
E = per unit electromotive force
K = a factor, expressed in per unit, designating torque per ampere and volts per revolution

Base quantities for the *MD612*—100 horsepower—30 minute motor are

T = 1,215 pound feet
I_A = 372 amperes
S = 433 rpm
E = 204.7 volts
K = 3.27 pound feet per ampere or 0.474 volts per rpm

Factor K of the *MD612* motor, as taken from test data, is given in Figure 1 of this discussion as a function of field and armature current. It will be noted that K varies greatly with changes in armature current at low field currents. Because of this inherent motor characteristic, the speed-torque curve for constant field excitation during lowering cannot be straight lines. Shown in Figure 2 of this discussion are calculated speed-torque curves for various amounts of motor field current with field and armature circuits connected across a 230-volt line. A value of 0.15 per unit resistance has been assumed to be in series with the armature. The amount of armature resistance determines the speed at which the stability limit occurs, but it has no effect on the maximum retarding torque the motor is capable of producing.

At the bottom of Figure 2 of this discussion motor torque and hook load have been correlated on the basis of the following assumptions: Motor base torque is required for hoisting full load on the hook. A driving torque of −0.10 per unit is required to lower the empty hook. At normal load, the hoist has a mechanical efficiency of 85 per cent so that lowering full hook load requires 0.7 per unit motor torque and, correspondingly, twice full hook load corresponds to 1.4 per unit motor torque.

A comparison between the author's curves and the calculated curves brings out several apparent discrepancies. The author states that curve *5L* is obtained with a field current slightly below full load current. He indicates a no-load speed of 120 per cent and a full load speed of 140 per cent. The calculations indicate that the field current of the *MD612* motor has to be reduced to 0.70 per unit to obtain comparable speeds. Under this condition the stability limit occurs slightly beyond twice normal hook load.

The author's curve *6L* indicates a no-load speed of approximately 175 per cent. Calculations show that it is necessary to reduce the *MD612* motor field current to 0.3 per unit to obtain such a speed. Under this condition the speed-torque curve is very unstable with the stability limit occurring at approximately full hook load.

It would be interesting to have the author's comments on the apparent discrepancies between the speed-torque curves of Figure 4 of the paper and Figure 2 of this discussion.

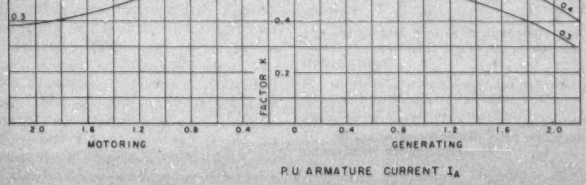


Figure 1. K-curves for *MD612* series motor, in per unit, based on 100-horsepower 30-minute ability

Reference

1. Dynamic Braking Control of D-C Series Motor—Calculation of Stability Limit, G. W. Heumann, W. P. Smith. AIEE Transactions, volume 65, July 1946, pages 454–57.

D. L. Pettit: Cooke and Heumann have presented a very well prepared discussion in connection with this paper. Their discus-

sion brings out points which well deserve covering.

A premise necessary in the design of a dynamic lowering circuit is that compromises in design are necessary to arrive at an appropriate solution. These compromises have to do with the practical limitations of the equipment and introduction of neutralizing measures which in themselves compromise the desirable characteristics of the control.

The control scheme presented represents a version of this network system which is characterized by simplicity and a minimum of equipment. Other variations are used where the conditions of the application indicate emphasis on certain features.

A coil burn-out of *2L* actually will open the armature circuit when the master switch is in positions 4, 5, or 6. This is not particularly alarming in most instances since the load would have been moving downward at approximately full-load hoisting speed or faster even if contactor *2L* were closed. Return of the master to positions 3, 2, or 1 recloses the regenerative circuit and brings the load under control. The net result is that the operator must be depended upon to return the master to a lower speed. If he does not do this, an unsafe condition will develop regardless of whether or not *2L* operated.

For those cases of extreme hazard, interlocking of *2L* and 1 *DB* contactors can be provided so that 1 *DB* does not open if *2L* does not pick up. This acts to keep 1 *DB* resistor loop intact in the regenerative circuit.

Regarding the comment on the failure of *1M*, it will be noted that should it fail to close, the motor will not be energized in speeds 1, 2, 3, and 4 lower. Hence the hoist will be inoperative in these points. When the master is moved to speed 5 or 6, contactor *2M* is closed. The field current may be so low as to not release the brake. The high-surge current to the armature through resistance 4 which is a very low ohmic value, will cause the overload to trip the under-voltage relay. Again, the crane becomes inoperative. Under these conditions, the crane operator is expected to call for maintenance. These conditions are considered safe.

In a variation of this circuit, we have already furnished an arrangement wherein the relay *LR* limited the lowering connection to the circuit for number 4 speed lower when heavy hook loads were involved. This in effect takes advantage of the improvement of stability limits by keeping a block of resistance (resistors 2 and 3 in parallel) in series with the line. This has the effect when extremely heavy overloads are involved of requiring the current returning to the line to flow through the resistor, raising the effective voltage above line voltage across the field and armature, thereby strengthening the field to improve the stability.

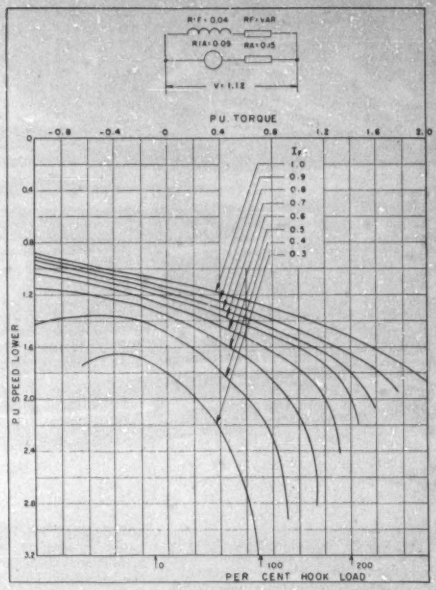


Figure 2. Dynamic braking lowering speed-torque curves for *MD612* motor for various amount of field excitation in shunt connection, based on 100-horsepower 30-minute ability

In the test data taken, full-load hoisting speed was 67 feet per minute. Actually, the points of data taken for the speed curves were not on an absolutely straight line, but no noticeable drooping of the curve was noted for values of hook load as indicated in the curves. Practical limitations of the setup limited further increases in loading.

In some cases, hook loads may reach values where stability limits are approached. As is borne out by the discussions, these stability limits are found at values of double or triple full load ratings. These cases deserve special consideration. The use of *LR* to limit the connection to speed 4, the reduction of ohmic value of resistance 3, or the complete neutralization of relay *LR* to eliminate weak field are considerations which should follow.

Direct comparisons of the stability limits cannot be made since tests were not conducted on an *MD612* motor. The actual motor used was a Westinghouse type *K* having a 30 minute rating of 45 horsepower. For purposes of comparison, a check with the test data indicates that on speed 5 at no-load speed per unit value of field current was 0.79 and on speed 6, it was similarly 0.41. When interpreting, allowance should be made for the difference in motors as well as possible errors in obtaining data.

The occurrence of the stability limit at about 200 per cent for speed 5 points up the advisability of using speed 4 for the limits for the operator if extreme abuse of the hoist is anticipated.

Relative Surface Voltage Gradients of Grouped Conductors

M. TEMOSHOK
ASSOCIATE AIEE

WITH the tendency toward higher a-c transmission line voltages, due to greater distances of power transmission and larger blocks of power transmitted, the type of line conductors to be used becomes of increasing importance. As the next voltage level above 287 kv evolves, the voltage gradient problem becomes more difficult. As conductor surface voltage gradients directly affect corona loss, the ultimate problem is that of corona loss. Increasing the conductor diameter is a solution; grouped conductors, two or more per phase, offer another solution.

To determine the degree grouped conductors affect the conductor voltage gradients is the aim of this paper. The approach is purely analytical, with the intent to indicate relative voltage gradients on conductor surfaces of grouped conductors to guide actual tests, and by comparison with actual tests, to determine the possibilities of calculating corona losses of grouped conductors. Major undertakings in the study of grouped conductors have begun in Sweden, France, Germany, and in this country.[1–6]

It is not the intention of this paper to cover the subject of inductance and capacitance values as affected by grouped conductors. The subject is introduced to present a more comprehensive and coordinated picture of the effects of grouped conductors, with regard to parameters and the effect on voltage gradients. The use of grouped conductors carries with it an important gain in the appreciably lower value of surge impedance, as the inductance of the line decreases and the capacitance increases. With the lower surge impedance and greater stability, a larger load may be carried.

Factors studied as affecting voltage gradients at the surface of the conductors are: number of conductors per phase; spacing between conductors of a group; spacing between phases; size of conductors; height above ground; presence and location of ground wires; and instantaneous values of voltages of each phase.

Assumptions

1. (*a*) All conductors are smooth.

(*b*) The area in circular mils is determined from the assumed smooth outside diameter of the wire, regardless of the construction, whether it be aluminum, copper, aluminum-steel, expanded, hollow, or solid. On any one comparison, the conductors are assumed to be geometrically similar.

2. Height of conductors and ground wires are assumed an average value over the span.

3. The line is perfectly transposed (for inductance and capacitance calculations).

4. Effects of line towers are ignored.

5. Spacing between phases is the distance between the centers of the conductor groups.

6. Intragroup conductor spacing is the distance between conductors of the same group.

7. Only the horizontal configuration of the line was studied.

8. (*a*) Two ground wires were used in the studies involving ground wires.

(*b*) Location of the ground wires was symmetrical with respect to the center and end phases.

(*c*) The ground wire protective angle was assumed to extend to the center of the conductor groups.

9. Unless otherwise specified, the term voltage gradient designates the maximum voltage gradient at the conductor surface.

10. Voltages are 60-cycle peak voltages unless otherwise specified.

11. Grouped conductors has been selected as the term applying to more than one conductor per phase. A study of literature in this field reveals the following terms used synonymously: bundle; split; multiple dual, duplex, triple (when two or three conductors per phase are used).

Scope of Study

1. Grouped conductors, groupings of 1, 2, 3, and 4 conductors per phase, with configurations as indicated by Figure 1:

2 conductors—horizontal configuration
3 conductors—triangular configuration
4 conductors—square configuration

2. Spacing between phases varied from 22 to 45 feet.

3. Intragroup conductor spacing varied from 8 to 24 inches.

4. Height of conductors above ground varied from 320 to 800 inches (26.67 feet to 66.67 feet).

5. Ground wire protection angles varied from 31 degrees to 45 degrees with one case extended to 90 degrees.

6. Conductor diameters were such as to keep the total conductor area per phase constant; conductor diameter ranges were:

1 conductor per phase—1.000 to 2.000 inches, each conductor
2 conductors per phase—0.707 to 1.414 inches, each conductor
3 conductors per phase—0.577 to 1.155 inches, each conductor
4 conductors per phase—0.500 to 1 000 inches, each conductor

Conclusions

1. (*a*) Total cross-sectional conductor area per phase (based on outside diameter of each conductor) has the greatest effect on conductor surface voltage gradient; the greater the total area, the smaller the voltage gradient

(*b*) If the total conductor areas per phase are kept constant, the most judicious combination of the variables studied, results in a maximum decrease in surface voltage gradient of about 20 per cent of grouped conductors over the equivalent single conductor.

2. Appreciable reduction in surface volt-

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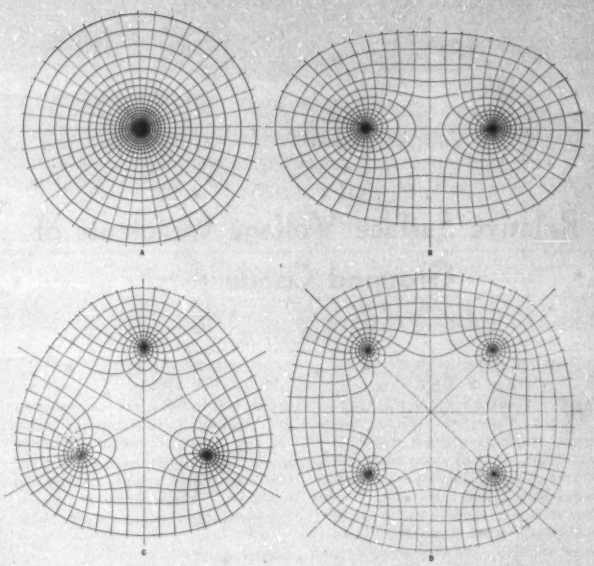
M. Temoshok is with the central station engineering divisions of the General Electric Company, Schenectady, N. Y.

The author wishes to acknowledge the aid in calculating given by Miss M. D. Grems and Mrs. Doris Frost.

Figure 1. Flux and equipotential lines of center phase group of conductors

Intragroup spacing, 18 inches
Height above ground, 66.7 feet
Spacing between phases, 32 feet
A—1 conductor per phase, 2.00 inches in diameter
B—2 conductors per phase, each 1.414 inches in diameter
C—3 conductors per phase, each 1.155 inches in diameter
D—4 conductors per phase, each 1.000 inch in diameter



age gradients results with the following:

(*a*) Greater number of conductors per phase, up to an intragroup conductor spacing of approximately 25 inches.

(*b*) Smaller intragroup conductor spacing down to approximately 8 inches, below which the gradient increases.

(*c*) Greater spacing between phases.

3. Negligible effects on surface voltage gradients result with the following:

(*a*) Ground wires of practical installation.

(*b*) Height of conductors above ground of practical installation.

4. (*a*) For a horizontal configuration of the three phase groups of conductors, the maximum voltage gradient is on the center phase group of conductors.

(*b*) For this maximum conductor surface voltage gradient calculation, the effects of the other two phases are small and effectively cancel each other, and can therefore be ignored.

5. The greatest reduction in inductance and increase in capacitance is with increasing number of conductors per phase. Other factors producing this variation to a lesser degree are:

(*a*) Larger intragroup conductor spacing.

(*b*) Smaller spacing between phases.

(*c*) Greater total conductor area per phase.

6. A correlation between calculated conductor surface voltage gradients and corona loss can be accomplished with a co-ordinated test program.

Discussion of Results

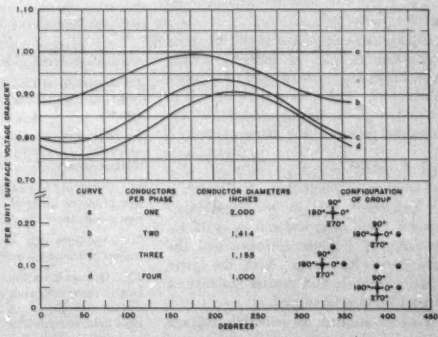
Conductor Surface Voltage Gradients

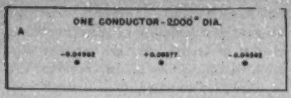
The analytical method used in the calculation of conductor surface gradients is outlined in the Appendix. Since at the beginning of the study it was not known what assumptions could be made, an exact analytical approach was made and calculations performed with the aid of punched card computing machines with resulting accuracy of about 1 part in 5,000. It was possible to establish approximate methods and equations which resulted in accuracies in the final value of voltage gradients to within less than one per cent of the exact value.

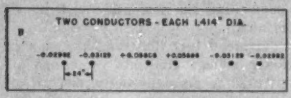
A good physical picture of the phenomenon associated with flux conditions in a group of conductors at the same potential is necessary for understanding the effects of grouped conductors. Figure 1 illustrates the lines of force and equipotential lines for 1, 2, 3, and 4 conductors per phase of the center phase group of the three phase line. Configurations A, B, C, and D of Figure 3 indicate the charge distribution among conductors at the instant of time when the voltage on the center phase is +1, and that of the end phases −0.5. No ground wires were applied to these groups. Configurations E_1 and E_2 indicate the effect of ground wires, for a configuration slightly different from that of configuration B. Study of Figure 3 in conjunction with Figure 1 indicates the charge distribution among the conductors and the resulting lines of force emanating from the conductors of the center phase. Between conductors the lines of force are distorted, resulting in a variable voltage gradient around the conductor periphery. As the number of conductors increases, the charge per conductor decreases resulting in fewer lines of force. The smaller flux emanates from conductors of

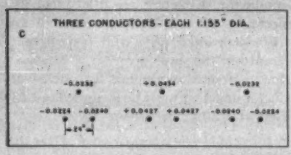
Figure 2. Surface voltage gradient distribution around conductor periphery

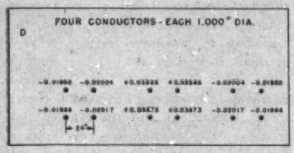
Per unit voltage gradient = 0.172 kv per inch per phase kilovolt applied.
Spacing between phases, 32 feet
Intragroup conductor spacing, 18 inches
Height of conductors above ground, 66.7 feet

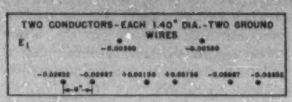












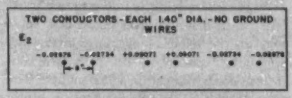


Figure 3(A) (left). Charge on conductors of various groupings with applied voltage of +1.0 on center phase and −0.5 on the end phases

Height of ground wires above conductors, 16 feet

Figure 3(B) (above). Effect of ground wires on charge distribution of two conductor per phase grouping

Height of ground wires above conductors, 16 feet

decreasing circumference, resulting in a surface voltage gradient that tends to remain constant.

Initiation of corona and corona loss is contingent on the voltage gradients at the surface of the conductor. For a smooth, round, single conductor, with spacings from other conductors and above ground as in practical transmission line applications, the voltage gradient is approximately uniform around the periphery of the conductor. The corona calculations can be based on this single value. In grouped conductors a different problem evolves. The voltage gradient is not uniform around the conductor periphery, as is evident in studying Figures 1 and 2, and a correlation must be established between corona loss and the variable voltage gradient. Although in theoretical calculations, relative losses can be determined by working with, for example, the maximum conductor surface voltage gradient, only actual tests can establish the relationship between corona loss and the voltage gradients. After test investigations in France,[2] a criterion has been established for the two conductors per phase grouping, wherein the mean value of the maximum and average voltage gradients is used. Whether this relationship applies to three and more conductors per phase is to be determined. In this paper, only the maximum voltage gradients are studied.

Effect of Number of Conductors and Intragroup Conductor Spacing. Although, in general, the surface voltage gradients are smaller for grouped conductors than for single conductors, it is possible to attain a surface gradient with grouped conductors that is higher than the gradient with a single conductor of equal area. Increasing the intragroup conductor spacing to approximately 25 inches or more will produce this result. Figures 4(A), 4(B), and 4(C) illustrate that the greatest reduction in maximum voltage gradient is with conductors per phase of a greater number, and that a peak results with an intragroup conductor spacing of under 12 inches. It should be noted that the point of maximum per cent reduction of voltage gradient based on the single conductor value is analogous to the point of minimum voltage gradient. This minimum point of the maximum voltage gradient does not exist at only one intragroup conductor spacing, but varies according to the other parameters of the system.

With parameters determined by an average higher voltage transmission line, the maximum voltage gradient decreases with respect to the single conductor value. For example, at a 15-inch intragroup conductor spacing, the voltage gradient decrease for the grouped conductors is approximately 3 per cent, 9 per cent, and 14 per cent for 2, 3, or 4 conductors per phase, respectively. If intragroup conductor spacing is increased to 24 inches and more, not more than about 10 per cent maximum can be realized with any grouping of conductors up to four in number. If the spacing is further increased, the grouped conductor proximity effect on charge and flux is diminished and on sufficient separation vanishes entirely.

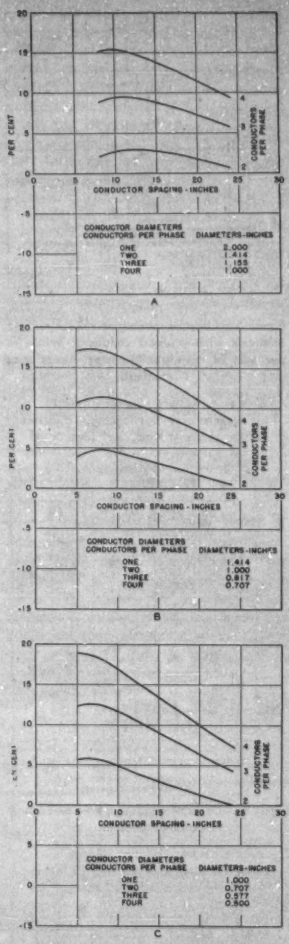


Figure 4. Surface voltage gradient reduction of grouped conductors based on value of single conductor of equivalent area

Spacing between phases, 32 feet
Height above ground, 66.7 feet

Intragroup conductor spacing is contingent on several major conditions: effect on conductor voltage gradient; effect on inductance and capacitance; striking and sticking of conductors due to wind; striking and sticking of conductors due to faults on the system; bridging of

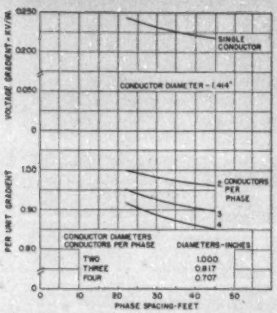


Figure 5. Effect of spacing on surface voltage gradients with grouped conductor values in per unit of corresponding single conductor gradients

One unit of phase kilovolt applied
Intragroup conductor spacing, 18 inches
Height above ground, 66.7 feet

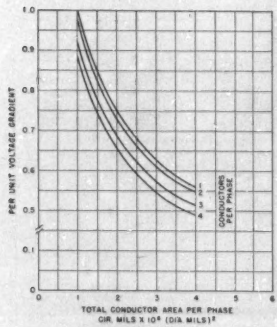


Figure 6. Effect of conductor area on surface voltage gradient

Spacing between phases, 32 feet
Intragroup conductor spacing, 18 inches
Height above ground, 66.7 feet

conductors due to icing. Theoretical and practical investigations on the latter three conditions have recommended a minimum conductor spacing of approximately 15 inches.[1,2,3,4] It is possible though, where icing is not a major problem, to utilize a shorter conductor spacing, at the expense of more intragroup conductor spacers, if the first two conditions contribute sufficient benefits. The spacers tend to keep a uniform intragroup conductor spacing between line towers on normal load, and to keep conductors from striking due to attractive forces between conductors with the abnormally high current flow of certain fault conditions.

Effect of Spacing Between Phases. For the case of a single conductor of Figure 5, the conductor surface voltage gradient decreases about 10 per cent due to the increase of spacing between phases from 22 to 45 feet. Based on the single conductor value of voltage gradient at each particular spacing, the greatest reduction in voltage gradient is with the grouping of four conductors per phase and at the phase spacing of 45 feet. If the 45-foot spacing between phases is determined by insulation levels, a reduction of surface voltage gradient of 15 per cent is possible with the use of a 4-conductor grouping instead of the single conductor, at an 18-inch intragroup conductor spacing.

Effect of Conductor Diameter. With equal area per phase, there is practically a constant relationship between the surface voltage gradients of each group of the grouped conductors and the single conductor over the range of areas from 1.0 $\times 10^6$ to 4.0×10^6 circular mils. For the cases illustrated in Figure 6, the total decrease of voltage gradient in each of the four cases is about 44 per cent. Therefore for ease in calculation of gradients where change of area effects are required, the grouped conductors of any number, can be converted to an equivalent single conductor and the percentage change calculated.

Figure 6 best illustrates the gain in lower voltage gradients commonly attributed to grouped conductors. If all conductor diameters are 1.00 inch, the decreases due to 2, 3, and 4 conductors are 27.0, 42.6, and 51.0 per cent, respectively. To attain the same percentage decreases due to grouped conductors the single conductor diameter must be increased to 1.45, 2.00, and 2.32 inches, respectively.

Effect of Height Above Ground. Over the range, 25 feet to 70 feet, of the average height of conductors above ground, the variation in maximum surface voltage gradient is slightly over 1 per cent as expressed by Figure 7(A). As the height decreases, the effect of ground becomes more pronounced and the gradient increases. The relationship of voltage gradients between each group of the grouped conductors and the equivalent single conductor is practically constant over the entire range.

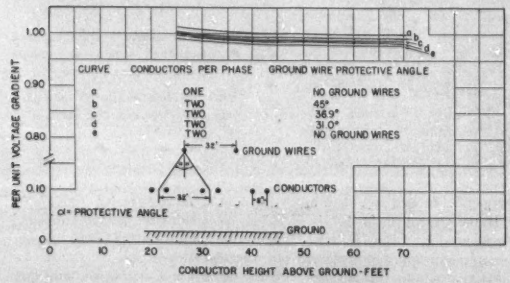
Effect of Ground Wires. Figures 7(A) and 7(B) indicate the small effect of ground wires. The range of ground wire location covers protective angles from 90 degrees (ground wire in line with conductors) to a protective angle of 31.3 degrees. If the protective angle is made smaller than 31.3 degrees, the gradient effect on the line conductors becomes almost negligible, under one-half per cent. For the usual protective angles the ground

Figure 7(A) (below left). Variation of surface voltage gradients with conductor height

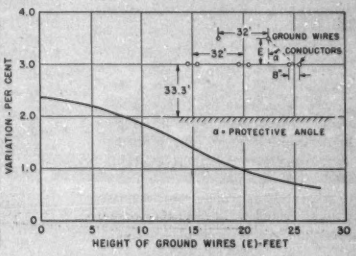
Diameter of conductors, 1.4 inches
Diameter of ground wires, 1.0 inch

Figure 7(B). Increase of maximum surface voltage gradient of conductors with ground wires over conductors without ground wires

Diameter of conductors, 1.4 inches
Diameter of ground wires, 1.0 inch



A



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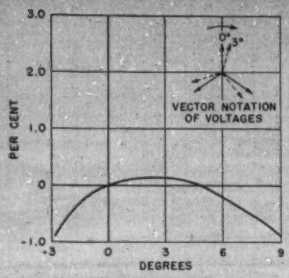


Figure 8. Variation of maximum surface voltage gradient with time

wires will cause an increase in voltage gradient of less than 2 per cent at the line conductor surface. Although Figures 7(A) and 7(B) depict the two group conductor, the same relationships apply to the three and four group conductors.

Figure 3(B), configurations E_1 and E_3, illustrates the charge on the ground wires and the effect of the presence of ground wires in increasing the charge slightly on the center phase line conductors.

Effect of Configuration and Instantaneous Voltage Values. On a horizontal configuration of three phases, the middle phase will inherently have the higher charge, and also the higher surface gradient. This difference is approximately 5 to 10 per cent for the range of parameters covered in the study. The greater number of conductors per phase, with larger spacing between phases, tends toward the greater difference.

For single-conductor 3-phase lines with horizontal configuration, the maximum voltage gradient of all three phases exists on the middle conductor when the peak voltage on that conductor is slightly less than unity. The same applies to the 2-conductor grouping. When the center phase voltage has progressed 3 to 5 degrees in time from its maximum peak value, the surface gradient is the maximum. Figure 8 indicates that the maximum value differs so little from the value determined when the voltage on the center phase is unity, that for practical purposes the voltage distribution can be +1.0 for the center phase and −0.5 for the end phases for the maximum gradient calculation. Since the 2-conductor case error for this assumption was so negligible, checking the single conductor error, the 3- and 4-conductor cases were not investigated.

Inductive Reactance, Capacitive Susceptance, and Surge Impedance

In a previous article,[6] complete curves were presented for inductive reactance of grouped conductors up to five per phase and capacitive susceptance up to two, for certain ranges of intragroup conductor spacing, spacing between phases, and conductor diameters. This paper extends the earlier work to conductors and lines suitable for higher voltages. Surge impedance, $\sqrt{L/C}$ is introduced to give a clearer conception of the total effect of grouped conductors on inductance and capacitance. In the calculations of inductance, the internal construction of the conductor was not considered. Calculations were based on the assumption of smooth, solid, copper conductors.

A sense of proportion as to the effect of change in inductance and capacitance is attained by study of Figure 10. Throughout the range of variation in spacing of phases and number of conductors per phase, an interesting phenomenon occurs. The per cent variation of inductive reactance, and surge impedance of the grouped conductors based on the single conductor values, is almost identical, so that per cent decrease of inductance can practically be used as the per cent decrease of surge impedance. This is due to the relationship between inductance and capacitance where approximately

$$\frac{1}{1 + \text{per unit change in capacitance}} = 1 - \text{per unit}$$

change in inductance. The per cent increase in capacitive susceptance therefore is appreciably greater than the corresponding decrease in inductive reactance due to parameter changes.

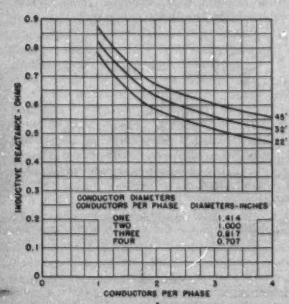
Figures 9(A) and 9(B) indicate the variation of inductive reactance and capacitive susceptance with number of grouped conductors. As the number of conductors per phase increases, the rate of change of inductive reactance and capacitive susceptance decreases.

As intragroup conductor spacing increases, capacitive susceptance increases, and inductive reactance decreases as indicated by Figures 11(A) and 11(B). This desirable trend is compensated by the increase of surface voltage gradients as intragroup conductor spacing increases, and intragroup conductor spacing may actually be limited by the higher voltage gradients if the spacing increases appreciably beyond 25 inches.

For the single conductor diameter change from one to two inches, a decrease results in inductive reactance of almost 10

Figure 9. Effect of number of conductors per phase and spacing between phases on inductive reactance and capacitive susceptance per phase per mile

Intragroup conductor spacing, 18 inches
Height above ground, 66.7 feet
(A) Variation of inductive reactance
(B) Variation of capacitive susceptance



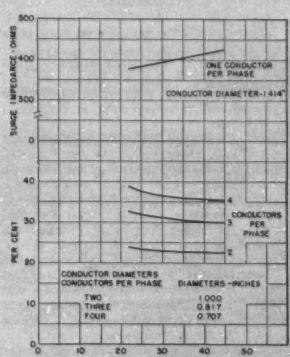
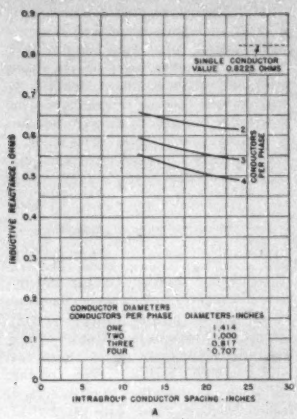
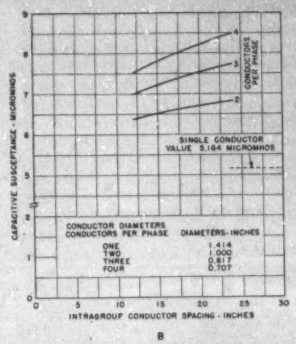


Figure 10. Effect of spacing between phases on surge impedance with per cent decrease of surge impedance of grouped conductors based on corresponding single conductor values





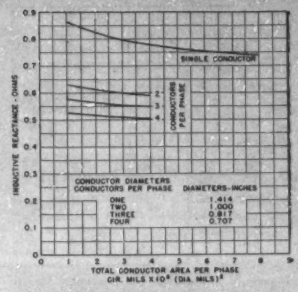


Figure 11. Effect of intragroup conductor spacing on inductive reactance and capacitive susceptance per phase per mile

Spacing between phases, 32 feet
Height above ground, 66.7 feet
(A) Variation of inductive reactance
(B) Variation of capacitive susceptance

Figure 12. Effect of conductor area on inductive reactance per phase per mile

Spacing between phases, 32 feet
Intragroup conductor spacing, 18 inches
Height above ground, 66.7 feet

per cent for a 32-foot spacing between phases. As the number of conductors per phase increases, the effect of conductor area decreases, as indicated by Figure 12. For the 4-conductor grouping, the one to four change in phase area, results in a decrease in inductive reactance of only 4 per cent.

Appendix

For a 3-phase system with two conductors per phase the equations for the calculations of charges with certain applied voltages to ground are as follows:

$$V_1 = P_{11}Q_1 + P_{12}Q_2 + P_{13}Q_3 + P_{14}Q_4 + P_{15}Q_5 + P_{16}Q_6$$

$$V_2 = P_{21}Q_1 + P_{22}Q_2 + P_{23}Q_3 + P_{24}Q_4 + P_{25}Q_5 + P_{26}Q_6$$

$$V_3 = P_{31}Q_1 + P_{32}Q_2 + P_{33}Q_3 + P_{34}Q_4 + P_{35}Q_5 + P_{36}Q_6$$

$$V_4 = P_{41}Q_1 + P_{42}Q_2 + P_{43}Q_3 + P_{44}Q_4 + P_{45}Q_5 + P_{46}Q_6$$

$$V_5 = P_{51}Q_1 + P_{52}Q_2 + P_{53}Q_3 + P_{54}Q_4 + P_{55}Q_5 + P_{56}Q_6$$

$$V_6 = P_{61}Q_1 + P_{62}Q_2 + P_{63}Q_3 + P_{64}Q_4 + P_{65}Q_5 + P_{66}Q_6$$

where

$V_1 = V_2$
$V_3 = V_5$
$V_5 = V_6$

$$P_{11} = P_{22} = P_{33} = P_{44} = P_{55} = P_{66} = 2 \log_e \frac{2h}{r}$$

$$P_{jk} = P_{kj} = 2 \log_e \frac{S_{jk}}{s_{jk}}$$

S_{jk} = distance between centers of the jth conductor and the image of the kth conductor

s_{jk} = distance between centers of the jth and kth conductors

Since the configuration of the transmission line is known, all values of P's can be calculated, assigned, leaving values of Q, charge, to be solved.

Inclusion of ground wires, and cases of three, or four conductors per phase, extends the number of terms and equations. The case of two ground wires, with four conductors per phase, results in 14 equations of 14 terms each.

As it was not known at the beginning of the study what assumptions and simplifications could be made in the solution for the charges, the Matrix Method[7] was selected, as this method introduces the inverse matrix and lends the greatest flexibility in the general solution of the problem, wherein the applied voltages to the phases were varied.

As the total number of cases to be studied was large, and the number of simultaneous equations was 6 to 14, the amount of work was almost prohibitive with the usual mathematical aids. The punched card calculators were applied admirably to the problem, with a maximum error of less than 0.02 per cent. Results indicated in Figure 3 and Figure 8 were derived, indicating the possible use of approximate methods with practically accurate results. Having discovered that the conductors of the middle phase group have the highest gradient when practically speaking, the voltage is unity on that phase, considerable simplification was possible. Symmetry of the end phases with the center phase exists, so that the six equations for two conductors per phase case with no ground wires, reduce to three; the nine equations for the three conductors per phase reduce to five; and the twelve equations for the four conductors per phase reduce to six. The maximum number of equations is seven for the case of four conductors per phase and two ground wires.

Since the voltages to be applied to the lines, for the symmetrical, simplified cases, are not varied but remain constant, the Matrix Method for these cases loses its virtue and a method such as the Method Based on Elimination[7] best lends itself to the solution of the desired charges.

The complete equation for the field intensity at a conductor due to other conductors, after the solution for the line charges has been made, is

$$E_1 = 2\sqrt{\left[\sum \frac{1}{m_{1n}} \cos \theta_{1n} Q_n + \sum \frac{1}{S_{1n}} \cos \theta_{1n} Q_n\right]^2 + \left[\sum \frac{1}{m_{1n}} \sin \theta_{1n} Q_n + \sum \frac{1}{S_{1n}} \sin \theta_{1n} Q_n\right]^2} \quad (1)$$

m_{1n} = distance between conductor 1 and all other conductors in the same phase group

S_{1n} = distance between conductor 1 and all other conductors of other phase groups including ground wires

θ_{1n} = angle between conductor 1 and all other conductors, including ground wires

Q_n = charge on all conductors except 1

Since the contributions to the field intensity of the conductors of the other two phases than the one in question, are usually under 0.5 per cent, ignoring these effects results in the simplified equation.[8]

$$E_1 = 2\sqrt{\left[\sum \frac{1}{m_{1n}} \cos \theta_{1n} Q_n\right]^2 + \left[\sum \frac{1}{m_{1n}} \sin \theta_{1n} Q_n\right]^2} \quad (1a)$$

The maximum field intensity at a conductor becomes:

$$E_{m1} = 2E_1 + \frac{2Q_1}{r_1} \quad (2)$$

where

Q_1 = charge on conductor 1
r_1 = radius of conductor 1

Equations 1 and 1a are accurate to intragroup conductor spacings as low as 8 inches for most cases. For intragroup conductor spacings such that

$$\frac{\text{intragroup conductor spacing}}{\text{conductor diameter}} < 6,$$

at least one per cent and up to 25 per cent error may result, so that an analytical approach is necessary to account for the field produced by other conductors not being uniform in the region of the conductor considered. Hillel Poritsky made an investigation of the 2-conductor case in 1932.[9]

For the number of conductors and configurations of the subject study, the equations for the maximum surface gradients become with good approximation:

$$\text{2 conductors } E_{m1} = \frac{4Q}{d} + \frac{4Q}{m} \tag{3a}$$

$$\text{3 conductors } E_{m1} = \frac{4Q}{d} + \frac{6.9284}{m} Q \tag{3b}$$

$$\text{4 conductors } E_{m1} = \frac{4Q}{d} + \frac{8.484}{m} Q \tag{3c}$$

d = diameter of conductor, inches
Q = average maximum charge among conductors in the phase
m = intragroup conductor spacing

The inductance to neutral per conductor is[8]

$$L_c = 0.08047 + 0.74113 \log_{10} \frac{24(S_{gm})^n}{d(m_{gm})^{n-1}} \quad \text{millihenrys per mile} \tag{4a}$$

Inductance per phase

$$L_\phi = \frac{L_c}{n} \tag{4b}$$

The capacitance to neutral per conductor is[8]

$$C_c = \frac{0.03883}{\log_{10} \dfrac{24(S_{gm})^n}{d(m_{gm})^{n-1}}} \quad \text{microfarads per mile} \tag{5a}$$

Capacitance per phase

$$C_\phi = nC_c \tag{5b}$$

where:

S_{gm} = geometric mean distance between conductors of different phases, feet
m_{gm} = geometric mean distance between conductors of the same phase, feet
n = number of conductors per phase
d = diameter of conductor, inches

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Discussion

W. A. Lewis (Illinois Institute of Technology, Chicago, Ill.): The paper by Temoshok is an interesting development of surface gradients in the neighborhood of grouped conductors which have been suggested for power transmission at higher voltages than those now in use in the United States. All of the developments, however, are based on the assumption that each of the conductors has a smooth outer surface. With the exception of the Hoover Dam line, a very large majority of the high-voltage lines now in use in the United States make use of stranded conductors, rather than the smooth conductors referred to in the paper. Although the type *HH* design approximates quite closely the smooth conductor, it is quite probable that stranded conductors may also be given serious consideration for the higher voltages, when grouped conductors are considered. To make the work of the paper of maximum usefulness, therefore, it is desirable to see what can be done to extend the analysis to the case of stranded conductors.

A fairly satisfactory first approximation of this result can be obtained in the following manner. Let us consider first a single conductor of a length very long in comparison with the spacing between the conductor considered and the other conductors of the circuit. If the spacing between conductors is quite large in comparison with the conductor radius, the voltage gradient at the surface of the conductor will be very nearly the same as for a single conductor with the return a concentric cylinder at a large distance. For the latter case, the surface gradient will be uniform all the way around the conductor, and can be calculated by determining the gradient due to a uniform line charge located at the center of the conductor.

For a stranded conductor, it may be assumed that the charge of the conductor, instead of being concentrated along a line at the center of the conductor, is divided into n equal parts where n is the number of strands in the outer layer of the conductor. If these n charges are distributed symmetrically about the center of the conductor at a radial distance from the center somewhat less than the radius of the circumscribing circle, it is possible to calculate the equipotential surfaces which result. By a little manipulation, it is readily possible to locate these charges at a distance from the center which will cause one of the equipotential surfaces to coincide very closely with the outer surface of the strands where the strands protrude. In the grooves between the strands, the calculated equipotential surface does not agree closely with the conductor outline, but in this region the gradient is much less, so that a high degree of accuracy is not necessary for the calculation of the maximum gradients.

Although this analysis will not apply rigorously for the case of grouped conductors, it will provide an approximate basis for determining the effect of stranding.

An analysis of this kind indicates that the maximum gradient at the surface of one of the strands is approximately 37 per cent greater than the average gradient at the surface of a smooth cylinder of the same outside radius. If the corona starting voltage and corona loss for both a stranded conductor and a smooth conductor are dependent only on the maximum gradient at any single point at the surface of the conductor, this analysis would indicate that corona would start for a stranded conductor at a voltage approximately 73 per cent of that for a smooth conductor. The test results available indicate that the corona loss for a stranded conductor is greater than that for a smooth conductor of the same radius, but the difference is by no means as great as that indicated by comparison of the maximum gradients.

It is therefore reasonable to conclude that the corona loss is not dependent on the maximum gradient only, but is a function of the manner in which the gradient varies from point to point around the surface of the conductor when the gradient is variable. As a first approximation, it might be assumed that the corona loss depended upon the average gradient at the surface of the conductor, the average being obtained as the mean of the maximum and minimum voltage gradients. With the method of calculation described, the minimum gradient is not obtained as accurately as the maximum gradi-

ent, but the average of the two might form a basis for analyzing corona-loss data. On this basis, the average gradient at the surface of a stranded conductor is given approximately by equation 1 as follows:

$$g_1 = \frac{1.07\, e_1}{r_n\left[\log_e \frac{d_{12}}{r_n} + \frac{0.136}{n}\right]} \quad \text{(average gradient)} \qquad (1)$$

where

e_1 = line-to-neutral voltage, kilovolts
g_1 = gradient, kilovolts per foot
n = number of strands in outer layer
r_n = radius of conductor circumscribing circle, feet
d_{12} = geometric mean distance to return conductor, feet

For a smooth conductor the gradient, both average and maximum, is given by equation 2 as follows:

$$g_1 = \frac{e_1}{r \log_e \frac{d_{12}}{r}} \quad \text{(uniform gradient)} \qquad (2)$$

where

r = radius of conductor, feet

When n is 12 or more, n being the number of strands in the outer layer of the stranded conductor, the average gradient at the surface of the stranded conductor is approximately seven per cent greater than it is for a smooth conductor of the same radius. Thus, the corona loss for a stranded conductor is approximately the same as that for a smooth conductor if the voltage is increased seven per cent. Such a figure is more nearly in agreement with the available test data than is the 37 per cent difference in voltage based upon the maximum gradient.

As a basis for further study, it may therefore be suggested that the method proposed could be used to include approximately the effect of stranding in analytic calculations of this type.

C. F. Wagner (Westinghouse Electric Corporation, East Pittsburgh, Pa.): While Temoshok had apparently not intended that his references be exhaustive of the subject, I would like to call attention to some of the earlier work done in Europe on bundle conductors. I refer, first, to the work of Markt and Mengele in Austria that was reported in *Elektrotechnik und Maschinenbau* in 1932 and 1938; and, second, to the Siemens-Schuckertwerke investigations. The work by Siemens-Schuckertwerke constitutes the most comprehensive studies on this subject and includes corona aspects, inductance and capacitance values, ice loading, and stringing methods. These investigations were reported by the Siemens-Schuckertwerke under the title "Bundelleitungen" in 1942, and were under the authorship of Mangoldt, Busemann, Burklin, Markt, and Kroner, and became available in America shortly after the cessation of hostilities.

Temoshok has stressed the maximum potential gradient and uses this as a criterion to compare different configurations of conductors. If this is the only quantity he was seeking, it could have been obtained in a somewhat simpler manner. While the method the author uses is beyond criticism from the standpoint of accuracy, much of the physical significance of the phenomenon is lost by this approach. Calculations made on a 2-inch diameter single-conductor 3-phase line of flat configuration in which the spacing was 45 feet between conductors, the conductors 70 feet above ground and with two ground wires 40 feet above the conductors and midway between them, showed that the potential gradient at the middle conductor was four per cent above the mean and the two outside conductors was two per cent below the mean. This example is merely cited to illustrate the more general and well-known fact that the gradient of the center conductor is the largest of the three. If one is concerned with maximum gradients only, detailed consideration need be given to the middle conductor only.

If it were assumed that the charges on all conductors comprising the center phase were equal, great simplification results. This assumption is rigorously true for a 2-conductor group in horizontal configuration, and can be shown by simple calculation to be correct within a small fraction of one per cent for most practical configurations. This is borne out by diagrams in Figure 3 which show, for example, that the charges on the 4-conductor group are equal to within about 0.1 of one per cent.

The comparisons made by Temoshok have all been made on the basis of maximum gradient, but he points out in referring to the work done in France that only tests can establish the relation between corona loss and voltage gradient. Since the voltage gradients on the three phases for flat configuration are not equal, the losses on the respective phases will likewise not be equal. The total loss on a line can probably be determined by using as a base the calculated gradients on each of the conductors and summing up the losses of all conductors. In view of this complication, it is likely that a good over-all compromise for practical calculation purposes might be obtained by assuming the phases located at the points of an equilateral triangle for which the spacing is equal to the geometric mean of the actual spacings, by neglecting the presence of ground and ground wires, and by assuming in addition that the charges on all conductors are equal. The maximum gradient obtained under these conditions might then be used as the criterion to compare performance or to compute the critical breakdown voltage. With these assumptions, the calculations become relatively simple.

For a perfectly smooth polished conductor for which there is only one per phase, according to Peek[10] the disruptive critical voltage to neutral (crest value) is

$$e = g_o r \log_e \frac{D}{r} \text{ in kilovolts to neutral} \qquad (1)$$

where

g_o = critical gradient, which Peek took as 30 kv per centimeter
r = radius of conductor
D = distance between conductors

If each phase consists of a 2-conductor group of spacing, S, separated a distance, D, the critical disruptive voltage is

$$e = g_o \frac{2}{1+2\frac{r}{s}}\, r \log_e \frac{D}{\sqrt{rs}} \qquad (2)$$

If each phase consists of a 3-conductor group

$$e = g_o \frac{3}{1+3.464\frac{r}{s}}\, r \log_e \frac{D}{3\sqrt[3]{rs^2}} \qquad (3)$$

If each phase consists of a 4-conductor group

$$e = g_o \frac{4}{1+4.242\frac{r}{s}}\, r \log_e \frac{D}{1.189^4\sqrt{rs^4}} \qquad (4)$$

If each phase consists of a 6-conductor group

$$e = g_o \frac{6}{1+5.310\frac{r}{s}}\, r \log_e \frac{D}{1.348^6\sqrt[3]{rs^4}} \qquad (5)$$

Each of the foregoing expressions is of the following form:

$$e = g_o \frac{n}{1+A}\, r \log_e \frac{D}{r_{eq}} \qquad (6)$$

The denominator in the logarithmic term, r_{eq}, is merely the geometric mean radius of the group. Thus the reciprocal of the logarithmic term is proportional to the capacity and to the total charge per group. Note that the n term is the number of conductors per group, and is present because it converts the total charge to the charge per conductor. In other words, the combination of the n term and the logarithmic term merely reflects the charge per conductor. The gradient around the periphery of the conductor due to the charge on itself is uniform. "A" expresses the influence of the charges on the other conductors comprising a group. Thus upon drawing a line through one of the conductors to the center of the group, the gradient along this line on the outward side is increased A per unit, and on the inside is decreased A per unit. Between these two extremes the gradient varies in a cosinusoidal manner around the periphery of the conductor.

In Peek's work he indicated that g_o is constant for conductors of diameters that would be used for high-voltage transmission. Peek's data and that of subsequent investigators raises considerable doubt as to whether g_o is constant in the range of low losses in which the transmission engineer is concerned. The indications are that g_o might well increase with decreasing radius. If that be so, this factor should be taken into consideration and would be regarded as a factor favorable to bundle conductors.

For visual corona, g, according to Peek is

$$g_v = 30\left(1+\frac{0.301}{\sqrt{r}}\right) \qquad (7)$$

The critical visual voltage, in kv to neutral, is then for say a 4-conductor group

$$e_v = 30\left(1+\frac{0.301}{\sqrt{r}}\right)\frac{4}{1+4.242\frac{r}{s}} \times r \log \frac{D}{1.189^4\sqrt{rs^4}} \qquad (8)$$

Most of Temoshok's comparisons are based upon equal cross-sectional areas of conductors of solid section. In the realm of

transmission voltages for which bundle conductors would be considered, both the conductors comprising the bundle and the competitive single conductors would not be of this construction. I refer to such conductors as the expanded ACSR (aluminum cable, steel reinforced), the sectored conductor commonly known as *HH*, and the I-beam conductor. The data can be converted to a radius basis, but with some inconvenience to the reader.

M. Temoshok (General Electric Company Schenectady, N. Y.): The results of Lewis in analyzing the effect of stranding on the conductor surface voltage gradients checks the results of W. S. Peterson[1] where the number of conductors is greater than 12. Another approach is the use of the grouped conductor method of flux plots as presented in the paper. This method of flux plots is similar to Lewis' analytical approach. In this case the conductors are grouped with a circular disposition and spaced one diameter apart between conductor centers. This flux plot result checks the maximum gradient value of 37 per cent greater than that of the smooth conductor, as derived by Lewis and by use of Peterson's equation.

One major purpose of the paper was to present relative gradients of grouped conductors compared with single. Similar relationships would have resulted had stranded conductors been considered instead of smooth, but with much greater complexity in their derivation. The stranding effects are important and Lewis' contribution has merit where absolute values of gradients are required.

A practical approach to introduce the stranding effect into corona loss equations is to calculate gradients for conductors as if they were smooth, then introduce a stranding factor. A factor of 0.92 was suggested by Peterson[1] for a well-weathered stranded conductor. This would check very closely Lewis' factor of 7 per cent, as putting the factors on a common base the reciprocal, 1/1.07, would be used.

Corona loss is known to exist at a voltage well below that value at which the gradient at the conductor surface is equivalent to the breakdown gradient of air, 53.6 kv effective, per inch. This loss is due to surface conditions, such as roughness or irregularities, of the conductor, resulting in corona loss known as brush loss. Likewise, corona loss may begin to exist at low voltages due the high surface gradients at each strand, but that these losses may not follow a well-defined pattern, until the higher voltages are reached where the corona losses begin to fit into fairly well defined patterns as suggested by Peterson and Peek.[1,2] A mean gradient determines the actual loss for single stranded conductors in the same manner apparently as that found by Cahen[3] for grouped conductors, and which was discussed in the main paper. The corona loss did not vary according to the maximum surface gradient of the dual conductors, but according to the mean gradient of the maximum and the average.

As Wagner pointed out it is a known fact, or it can be easily indicated, that the maximum surface gradient of the center conductor is the highest of three single conductors in a horizontal configuration. Whether a perfectly analogous situation existed with grouped conductors and what relationship existed between the maximum surface gradients among phases and among conductors of a group was one problem of this study. This could only be accomplished by an exact approach, and from which future approximations could be justified.

Wagner's method, of simplifying conditions for corona calculations by placing conductors at the points of an equilateral triangle and assuming equal charges on all conductors, has merit. But the actual mean maximum charge on the end conductors can differ from the mean maximum charge on the center conductors by approximately 10 per cent for the three or four conductors per phase groupings, and among the conductors of the end groups approximately a 5 per cent variation in charges among the individual conductors may exist. As these variations differ materially from those of the one and two conductor groupings, one equivalent representation of all groupings may not be sufficiently accurate.

There is evidence in test data that an apparent increase in g_o exists with decreasing radius. If test corona losses are plotted as functions of the conductor surface gradients, and referring all other parameters to a common base, there is a tendency for smaller conductors to have smaller losses. As stated by Wagner, this would indicate lower losses for grouped conductors compared with losses of the equivalent single conductor.

The objection of Wagner to the use of total cross-sectional area of conductors per phase as a basis of comparisons among grouped and single conductors, is not apparent. This method is flexible and can be used to compare any types of conductors and any number of conductors per phase. If resistance values or material weight enter as criterions, different values of area may result for each grouping, but the same reference base can be used. In fact the comparisons where actual diameters are used are the ones to heed, unless the assumption is made, as in the paper, that comparisons are made between conductors of the same kind.

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Mercury-Arc Frequency Converter for Induction Heating of Metals

S. R. DURAND
MEMBER AIEE

J. B. RICE
MEMBER AIEE

Synopsis: An analysis is presented of the theory of operation of mercury-arc equipment for converting 3-phase power of commercial system frequencies into single-phase power of a frequency of the order of one kc for use in induction heating and melting applications. Mathematical relationships are derived to show the effect of both low- and high-frequency commutation and the frequency response of the grid phase angle control on the high-frequency output voltage and regulation. The self-tuning characteristics of the circuit are analyzed and a comparison is made between its response and that of a motor-generator set to changes in the load circuit inductance. Some practical considerations are discussed in connection with the installation and operation of mercury-arc frequency changing equipment of several hundred kilowatts rating for induction melting service.

INDUCTION melting of high-grade alloys has been accomplished in the United States during the past 25 years by converting 60-cycle electric power into a frequency of 960 cycles, in general, by means of motor-generator sets. Within the last few years, a static-type frequency changer has been developed to obtain electric power at a frequency of the order of one kc for induction heating and melting applications.[5,12] An analysis of the circuit theory and performance characteristics of this electronic frequency conversion equipment has been of importance in connection with the design of equipment of several hundred kilowatts rating for installations in industrial plants and foundries.

Operation Viewed as a Steady-State Phenomenon

The basic circuit used at the present time to obtain single-phase kilocycle frequency power from a 3-phase 60-cycle network is shown in Figure 1. The 60-cycle transformer is a 3-phase rectifier transformer with a zigzag secondary winding. The high-frequency transformer is a 4-winding single-phase transformer designed for operation at the output frequency usually of the order of 1,000 cycles. The three primary windings of this transformer are connected to the anodes of a mercury-arc converter unit. This converter unit is of a type well known in the transportation, electrochemical, steel, and mining industries, where it is used extensively for rectifying a-c into d-c electric power. A reactor is included in the circuit for interconnecting the mercury pool cathode of the converter unit with the neutral terminal of the zigzag winding of the 60-cycle transformer. The output winding of the kilocycle-frequency transformer is connected to the induction heating coil and capacitor circuit.

Essentially, the basic circuit consists of a 3-phase single-way rectifier, which converts power from the 60-cycle network into direct current, and a single-phase inverter,[1,6] which converts direct current into power at a frequency desired for the induction heating application. The inverter operates into a tuned circuit consisting of the inductance of the heating coil and capacitance of a bank of capacitors in parallel with the coil. Voltage for control of the mercury-arc converter grids is obtained from the tuned output circuit and thus is fed back in a somewhat similar manner to that employed in the well-known tuned plate oscillator.

Assuming that the circuit is operating under steady-state conditions, the voltages e_{a1} and e_{a2} from the center tap of one of the high-frequency transformer windings to anodes 1 and 2 may be shown as in Figure 2. Also assume that during the time illustrated in Figure 2, phase A of the 60-cycle zigzag transformer winding is more positive than either phase B or C, and that at time t_0 to t_1, anode 2 alone is carrying current. Neglecting arc drop, the cathode of the mercury-arc converter unit is thus at the same potential as anode 2 in this time. Anodes 4 and 6 will be negative with respect to the cathode because, neglecting the impedance drop in the transformer windings, they have the same high-frequency voltage as anode 2 but less positive 60-cycle potentials. Anodes 3 and 5, which have the same high-frequency voltages as anode 1, are similarly less positive than anode 1 because they also have less positive 60-cycle potentials.

It is convenient with this circuit to ground the cathode, and, from an analytical standpoint, to consider the cathode as being always at zero potential. Thus, the cathode potential may be drawn as the zero line of Figure 3, and the voltage of anode 1 to cathode e_{k1} is represented by the solid line. During the nonfiring period of anode 1 (while anode 2 is firing) this voltage is twice the anode-to-neutral voltage e_{a1} of the high-frequency transformer.

From t_0 to t_1, anode 2 is firing, and anode 1, even though it is more positive, is prevented from firing because a negative potential is maintained on its grid. At the instant t_1, anode 1 is released by permitting its grid to become positive, but because of the leakage inductance of the transformer, the current flowing to anode 2 cannot be instantly extinguished, so that from time t_1 to t_2 both anodes are carrying current and are effectively at cathode potential. This is called the overlapping period.

At instant t_2, commutation of the arc current from anode 2 to anode 1 is completed, and anode 2 stops carrying cur-

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S. R. Durand and J. B. Rice are both with the Allis-Chalmers Manufacturing Company, Milwaukee, Wis.

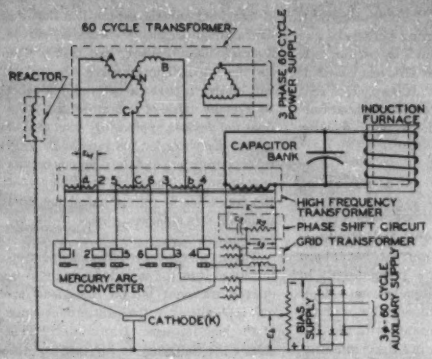


Figure 1. Circuit diagram of mercury-arc converter for induction heating

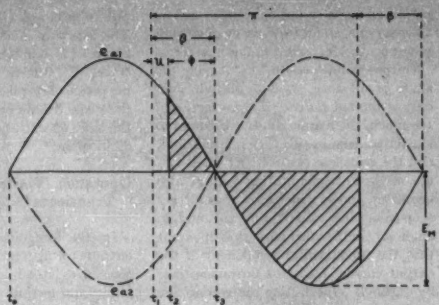


Figure 2. High-frequency transformer-primary voltage during firing period

rent. Anode 1 remains at cathode potential, and anode 2 assumes a value e_{13} equal to twice the phase-to-neutral voltage, e_{a1}. Anode 1 carries current until instant t_4 when anode 2 is again released by its grid, and commutation of the arc current from anode 1 back to anode 2 will take place.

Referring again to Figure 2, the shaded areas show the value of e_{a1} during the time between t_2 and t_4 that anode 1 is firing. At t_4, voltage, e_{a1}, changes from a positive to a negative value. Even though e_{a1} becomes larger in magnitude than the phase-to-neutral 60-cycle voltage, e_{t1} at some time during the firing period, the inductance of the cathode reactor is designed to be large enough to keep current flowing from anode 1 to the cathode. The fact that the average value of the voltage, e_{a1}, is negative during the firing period of anode 1, as may be seen from the relative magnitudes of the shaded areas, shows that power is being delivered to the load.

It is evident that during the overlapping period t_1 to t_2 which persists during the angle u, the voltage input to the high-frequency circuit is zero, so that even though anode 1 is released at t_1, the high-frequency voltage is not developed until time t_2 at an angle ϕ equal to $(\beta-u)$. The angle ϕ is termed the "margin" angle, and the corresponding time from t_2 to t_3 is designated the "margin time" or "available deionization time." In this time, anode 2, which has ceased firing, is at a negative potential, and its grid must gain blocking control through deionization of the space surrounding it so as to prevent re-ignition of the arc after time t_3 when the voltage on anode 2 becomes positive.

Assuming that the inductance of the cathode reactor is large enough not only to smooth out the high-frequency current but also to substantially eliminate the 180-cycle ripple from the 3-phase 60-cycle rectified output, the high-frequency voltage produced by inversion can be considered as derived from a smooth d-c voltage. If the transformer windings are considered to have negligible resistance and the cathode reactor is assumed to be a pure inductance, the average value of the high-frequency voltage during 1/2 cycle (π radians) must be equal to E_d. This follows from the fact that the average voltage across a perfect inductance must be zero.

The value of the high-frequency voltage may be determined by integrating the sine wave within the proper angular limits. Designating the maximum value of the high-frequency voltage by E_m, an expression for this voltage, as represented by the two parts of the shaded areas of Figure 2, may be written and equated to the d-c voltage as follows (see list of symbols in Appendix):

$$\frac{E_m}{\pi}\left(\int_0^{\pi-\beta} \sin\theta d\theta - \int_0^{\phi} \sin\theta d\theta\right) = E_d \tag{1}$$

Substituting

$$\cos(\pi-\beta) = -\cos\beta$$

and

$$\phi = \beta - u$$

$$\frac{E_m}{\pi}\left[(\cos\beta + \cos(\beta-u)\right] = E_d \tag{2}$$

$$E_m = \frac{\pi E_d}{\cos\beta + \cos(\beta-u)} \tag{3}$$

or the rms value of the high-frequency voltage is

$$E_{hf} = \frac{\pi}{\sqrt{2}} \frac{E_d}{\cos\beta + \cos(\beta-u)} \tag{4}$$

The overlapping angle u depends upon the transformer leakage impedance, the value of the cathode current, and the angle β (which determines the commutating voltage). The angle u, therefore, cannot be changed at will in operation. However, the magnitude of the output voltage may be varied within limits by controlling either the d-c voltage, E_d, or the angle of advance, β, and both methods are used. E_d can be raised or lowered by changing taps on the 60-cycle transformer, or it can be lowered by applying 60-cycle phase control to the grids. This method of phase control to regulate the part of the 60-cycle voltage wave over which the anodes fire and carry current is well known in connection with the appli-

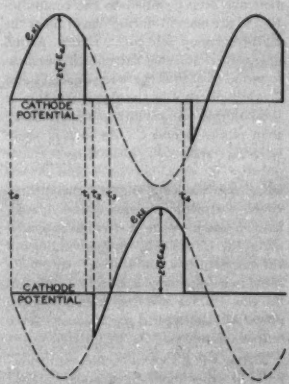


Figure 3. Anode-to-cathode voltage wave shape

cation of mercury arc converters in rectifier service, and a wide range of smooth power control is possible in this manner.

Output voltage control by varying the angle of advance, β, is applicable over only a limited range. If β is decreased too much, the margin angle ϕ becomes so small that deionization time is insufficient, and the grids lose control. At small values of β, the commutating voltage also decreases, and so u increases, thus further shortening the value for a safe margin angle ϕ. On the other hand, it is apparent that the fundamental component of the output current leads the output voltage by the angle ϕ (neglecting the overlapping angle u), so that if the angle ϕ is increased, the output current becomes more leading. The power factor of the output is approximately cos ϕ. This places a limitation on the range of control available by increasing the angle of advance, β, and consequently increasing the value of ϕ, since increasing β too much may cause the kilovolt-ampere rating of the high-frequency transformer to become excessive for a given kilowatt output.

The fact that the output current leads the voltage by the angle ϕ results in the operating frequency adjusting itself to a value sufficiently above the resonant frequency of the load circuit so that the current will become leading by this amount. The frequency shift required to produce a given angle ϕ between the current and voltage depends upon the Q (quality) of the heating coil. The value of $Q(\omega L/R)$ for a typical induction furnace is about ten. As a rule, the operating-frequency will be about three per cent higher than the resonant frequency of the load.

The discussion of the theory of operation has been confined to the commutation of arc current during one cycle of the high-frequency output. Under actual operating conditions, current will be transferred several times between anodes 1 and 2, while phase A of the zigzag winding of the 60-cycle transformer is more positive than phases B and C. As each of these 60-cycle transformer phases becomes more positive in turn, anodes 1 and 2 will cease commutating the arc current between them, and anodes 3 and 4, and, later, 5 and 6 will take over successively. Since the three high-voltage windings of the single-phase transformer supplying these pairs of anodes is closely coupled to the low voltage winding, high-frequency power will be supplied continuously to the output circuit.

Figure 4 is a typical oscillogram showing (A), the anode-to-cathode voltage, (B), the grid-to-cathode voltage, (C), the anode current of one anode, (D), the output voltage, and (E), the output current. The alternate conducting periods of one anode of a pair is shown by oscillograph trace, (C), during the 60-cycle conducting period of its phase. It is apparent that the phase conducting period is about one-third of a 60-cycle period as marked on the oscillogram.

Operation Viewed as a Series of Transients[3,4]

In the foregoing, the high-frequency output voltage and current have been treated as steady-state phenomena. In reality, the output voltage is composed of a series of transients during the conducting periods which alternate with another series of transients during the commutating periods. Each conducting period transient may be thought of as being initiated by the sudden recovery of voltage across the primary of the high-frequency transformer at the instant commutation is completed. Each commutating period transient is initiated by the sudden short-circuiting of the transformer primary winding when the entering anode fires at the beginning of the commutating period.

Each conducting period voltage transient, e, is a portion of a damped sine wave, of the form

$$e = -E_m \epsilon^{-t/\tau} \sin(\omega t - \phi) \qquad (5)$$

where $\tau = 2L/R$ and L and R are the inductance and equivalent resistance respectively of the loaded inductor coil. Typical time constants, τ, encountered in practice are in the neighborhood of four milliseconds, while the firing period lasts less than 1/2 millisecond for 1,000-cycle operation. For these values, the damping factor, $\epsilon^{-t/\tau}$, at the end of each transient would be $\epsilon^{-1/8} = 0.88$. Although this damping is appreciable, the error introduced by assuming undamped sine waves in the previous analysis is negligible for two reasons. First, the average value given by that analysis is correct because it is based only on the assumption that the average high-frequency voltage equals the average direct voltage, and that is true for the actual damped wave. Second, it can be shown that the ratio of rms value to average value of a damped sine wave does not deviate appreciably from that of a true sine wave for the interval covered by each transient and the damping factors encountered. Therefore, it follows that the rms value of voltage calculated from equation 4 is sufficiently close to the actual rms voltage.

The high-frequency voltage during commutation is of the same form as the conduction transient, but the tuned load circuit is paralleled by the leakage impedance of the high-frequency transformer. This causes the frequency to increase and the time constant to decrease somewhat, but the duration of the commutation transient is so short (less than 1/10 millisecond) that the effect of damping is entirely negligible for practical considerations.

Voltage Regulation

The voltage regulation of the entire circuit during a normal melting cycle may be thought of as made up of three components: regulation of the direct voltage due to increase of the 60-cycle overlap with increasing load; equivalent IR drop in the direct voltage due to copper losses in the 60-cycle-transformer, cathode reactor, and high-frequency transformer; and regulation due to the increase of high-frequency overlap with increasing load. The converter arc voltage drop, which is relatively constant over the normal load range, theoretically also should be subtracted from the direct voltage, but because of operation at a relatively high voltage, the change in arc drop is less than 0.2 per cent and therefore can be neglected for calculation of regulation. Each component of regulation is analyzed separately, and an expression for the total voltage drop is derived.

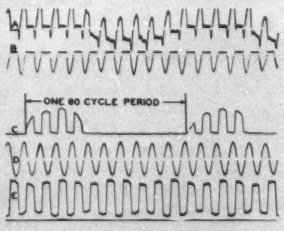


Figure 4. Oscillogram of high-frequency voltages and currents

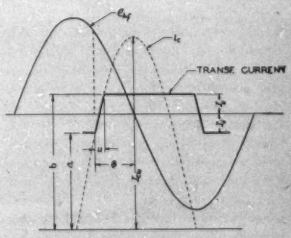


Figure 5. High-frequency commutation

The expression for voltage drop of the direct voltage of a three-phase single-way rectifier due to overlap is well known:

$$E_{x1}=\frac{I_d X_{c1}}{2\frac{\pi}{3}} \tag{6}$$

where X_{c1} is the commutating reactance of the 60-cycle rectifier transformer and I_d is the cathode current.

The resistance losses in all the transformer and reactor windings appear as a reduction in the direct voltage which is proportional to the cathode current and therefore can be represented by:

$$E_r=I_d r \tag{7}$$

$$r=\frac{P_r}{I_d^2} \tag{8}$$

where P_r is the total copper loss of all the windings.

The average direct voltage under load (neglecting arc drop) can now be expressed as follows:

$$E_d=E_{d0}-E_{x1}-E_r \tag{9}$$

$$E_d=E_{d0}-I_d\left(\frac{3}{2\pi}X_{c1}+r\right) \tag{10}$$

Where E_{d0} is the no-load direct voltage whose well-known expression is:

$$E_{d0}=\frac{3\sqrt{2}}{\pi}E_s\sin\frac{\pi}{3}=\frac{3\sqrt{3}}{\pi\sqrt{2}}E_s=1.17E_s \tag{11}$$

In order to determine the voltage drop due to high-frequency overlap, high-frequency commutation first must be analyzed. As previously explained in connection with Figure 3, during the commutation interval from t_1 to t_3, both anodes are carrying current which effectively constitutes an electrical connection between them. Since the voltage on the output winding of the transformer is sustained by the large capacitor bank, a short-circuit current will start to build up which will flow through the high-frequency primary windings and the two anode arcs in such a direction as to increase the current of anode 1 and decrease that of anode 2. When the current of anode 2 becomes zero and that of anode 1 becomes equal to I_d, commutation is completed. During this time, the transformer primary current in effect has changed from $-I_d$ to $+I_d$ since the ampere turns have changed from $-nI_d$ to $+nI_d$, n being the turns from one terminal to the center tap. The time required for commutation can therefore be regarded as the time for the short-circuit current in the primary (one terminal to the center tap) to build up to $2I_d$ This is illustrated in Figure 5. As shown, the commutating current, i_c, will be offset from zero to satisfy the condition that $i_c=-I_d$ at zero time (when anode 1 fires). The resistance of the windings is neglected so that the commutating current lags the secondary voltage by 90 degrees. From Figure 5, the following relations are evident:

$$a=I_{cm}\cos\beta \tag{12}$$

$$b=I_{cm}\cos(\beta-u) \tag{13}$$

I_{cm} is indicated on Figure 5.

$$b-a=2I_d=I_{cm}\Big[(\cos(\beta-u)-\cos\beta)\Big] \tag{14}$$

Therefore

$$\cos(\beta-u)-\cos\beta=2\frac{I_d}{I_{cm}} \tag{15}$$

$$\cos\beta+\cos(\beta-u)=2\left(\frac{I_d}{I_{cm}}+\cos\beta\right) \tag{16}$$

But

$$I_{cm}=\sqrt{2}\,I_c=\frac{\sqrt{2}E_M}{X_{c1}} \tag{17}$$

where X_{c1} is the commutating reactance of the high-frequency transformer in primary terms.

$$X_{c1}=\omega_c L_c \tag{18}$$

In computing X_{c1}, it must be remembered that the angular frequency, ω_c, during commutation is considerably greater than the natural angular frequency, ω, of the tuned circuit because the inductance of the heating coil is paralleled by the leakage inductance of the transformer, so that

$$\frac{\omega_c}{\omega}=\left(1+\frac{L}{L_c}\right)^{1/2} \tag{19}$$

In a typical case, the commutating inductance, L_c, is slightly less than the heating-coil inductance, L, so that the ratio of ω_c to ω is approximately $1^1/_2$. This increase in frequency during commutation causes a slight distortion in the voltage wave. However, the voltage is shown as a pure sine wave in Figure 5 and β is defined as the angle measured from the zero of this wave to the firing point.

Now, substituting equation 17 in 16:

$$\cos\beta+\cos(\beta-u)=2\left(\frac{I_d X_{c1}}{\sqrt{2}\,E_M}+\cos\beta\right) \tag{20}$$

and substituting equation 20 in 4:

$$E_M=\frac{\pi}{\sqrt{2}}\frac{E_d}{2\left(\frac{I_d X_{c1}}{\sqrt{2}\,E_M}+\cos\beta\right)} \tag{21}$$

from which

$$E_M=\frac{\pi}{2\sqrt{2}\cos\beta}\left(E_d-\frac{2}{\pi}X_{c1}I_d\right) \tag{22}$$

Thus, the voltage drop due to high-frequency overlap can be regarded as an equivalent drop in the direct voltage which is given by:

$$E_{x2}=\frac{2}{\pi}X_{c1}I_d \tag{23}$$

Then, an equivalent direct voltage, including the drop E_{x1}, can be written:

$$E_d'=E_d-\frac{2}{\pi}X_{c1}I_d \tag{24}$$

Substituting for E_d from equation 10:

$$E_d'=E_{d0}-I_d\left(\frac{3}{2\pi}X_{c1}+r+\frac{2}{\pi}X_{c1}\right) \tag{25}$$

The total per cent regulation of the output voltage is therefore:

$$\text{Per cent regulation}=\frac{E_{d0}-E_d'}{E_d'} \tag{26}$$

A convenient expression for calculating the high-frequency voltage under load is obtained by substituting from equations 24 and 25 in 22:

$$E_M=\frac{\pi}{2\sqrt{2}\cos\beta}\left(E_{d0}-I_d\times\left[\frac{3}{2\pi}X_{c1}+r+\frac{2}{\pi}X_{c1}\right]\right) \tag{27}$$

The no-load voltage is obviously:

$$E_{M0}=\frac{\pi}{2\sqrt{2}\cos\beta}E_{d0} \tag{28}$$

The same expression is obtained from equation 4 when $u=0$ and the reactance and resistance drops of the direct voltage are zero so that $E_d=E_{d0}$. In equation 27 E_M is the output high-frequency voltage expressed in terms of the primary voltage of the high-frequency transformer where the primary is considered as being the portion of the winding from the center tap to one terminal.

Grid Control Frequency Response

The inductance of the load coil varies throughout the progress of a heat because of the varying inductance of the charge. The value of this inductance depends upon the size and shape of the metal pieces making up the charge, the magnetic characteristics of the charge material, and its temperature. There are so many variables that it is not practical, at least for melting furnaces, to calculate the resultant inductance except for the empty coil and the coil filled with molten metal. In general, however, in melting nonmagnetic materials the frequency continuously in-

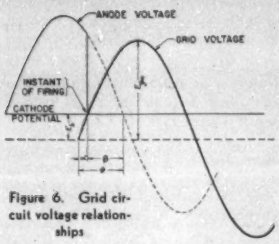


Figure 6. Grid circuit voltage relationships

creases as the pieces successively fuse together until finally a single molten cylinder is formed. In melting magnetic materials, the frequency is lower for a cold-charge of highly magnetic material than for an empty coil and continues so until the charge reaches the Curie-point temperature where the charge becomes nonmagnetic, at which time the frequency rises quite suddenly and then increases gradually until the fully molten condition is reached.

Any phase-shifting circuit consisting of resistance and capacitance or resistance and inductance will give a phase shift which varies with frequency. The grid voltage feedback circuit used for the induction heating converter consists of a resistor, R_g, in series with a capacitor, C_g, as shown in Figure 1. The resistor voltage, E_g, is applied to the grids. In this circuit:

$$\tan\theta = \frac{X_g}{R_g} = \frac{1}{R_g C_g \omega} \tag{29}$$

$$E_g = E\cos\theta = \frac{R_g C_g \omega}{\sqrt{1+(R_g C_g \omega)^2}} \times E \tag{30}$$

It is evident that both the angle, θ, and the magnitude of the grid voltage, E_g, vary with ω. Figure 7 is a circle diagram showing the locus of the grid voltage as the value of the angular frequency, varies. In addition to shifting the phase of the grid voltage, a d-c negative bias is applied to the neutral of the grid transformer so that the voltage from grid to cathode is offset from zero in a negative direction as shown in Figure 6. This negative bias retards the firing angle by an angle:

$$\alpha = \sin^{-1}\frac{E_b}{E_{gm}} \tag{31}$$

Thus, the net angle of advance (or firing angle), β, is:

$$\beta = \theta - \alpha \tag{32}$$

Now, an inspection of the circle diagram shows that as the frequency increases, θ decreases but the magnitude of E_{gm} increases so that E_b/E_{gm} becomes smaller and therefore α decreases. If the bias voltage is small compared to E_{gm}, so that θ is almost equal to β, the latter effect is small, and the net effect is a decrease in β with a resultant decrease in E_M. (See equation 27.) On the other hand, if θ is made nearly 90 degrees (therefore $\alpha \cong 90° - B$), α will change rapidly with ω, and θ, as evident from the circle diagram, will change very little. This means that as ω goes up, β will also increase causing E_M to increase, too. By properly proportioning θ and E_b between these extremes, it is possible to make β approximately independent of ω over the operating range of frequency or, if desired, to produce an "overcompounded" or "undercompounded" effect. This can be seen by a study of the circle diagram.

The small circle with its center at O is made to have a radius equal to E_b when the length of OB represents E_{gm}. OB also represents the phase position of the grid voltage and OA represents the phase position of the anode voltage. The angle which CB makes with OA is:

$$\beta = \theta - \alpha$$

and is therefore equal to the firing angle. It is easily seen that if E_b is proportioned so that CB is tangent to the large circle at B, the angle β will remain approximately constant as the point B moves up or down over a narrow range along the large circle when the frequency varies.

Other methods of compensating for the effect of frequency on the firing angle, β, have been used but have the disadvantage that they rely on a fixed relationship between the load change and frequency change.

Power Relationships in the Load Circuit

The output circuit connected to the high-frequency transformer consists of an inductor coil, L, surrounding the metallic charge and a parallel capacitor band, C, to tune the circuit to the desired frequency. An equivalent circuit for such a load is shown in Figure 8. The resistance R_L represents the power delivered to the charge, the loss in the inductor coil, and the stray losses in the structure surrounding the coil. There is much in the literature[2,7,8] on the theory of current and flux distribution in cylindrical and flat slab loads, and on methods of calculating the equivalent resistance of, and the power absorbed by, such loads and of selecting the best frequency. It will suffice here to say that skin effect causes the current induced in the load to be concentrated near the surface, thereby increasing its equivalent resistance.

This skin effect becomes more pronounced with increasing frequency, causing the power absorbed by the load to increase for a given magnetizing intensity. This is the reason that frequencies higher than 60 cycles are advantageous for most induction heating applications.[9]

The principal applications of mercury-arc frequency-changing equipment at the present time have been for induction melting of alloy steels. In practice, the only conditions for which it is possible to make a reasonably accurate calculation of power input to the furnace are: for an empty furnace; and for a fully molten charge. The case of the fully molten charge then becomes the one of a solid cylindrical charge for which formulae are available for calculating fairly accurately the value of R in Figure 8.[2,7,8,10]

The error will be small if we assume that the circuit will oscillate at the angular frequency $\omega = 1/\sqrt{LC}$ where L is the inductance of the coil with molten charge. Also for typical loads, ωL is of the order of ten times R so that the error is small if I_L is calculated by

$$I_L = \frac{E}{\omega L} = \frac{E\sqrt{LC}}{L} = \frac{E}{\sqrt{\frac{L}{C}}} \tag{33}$$

The power input to the charge is given by

$$P = I_L^2 R = \frac{E^2 C}{L} R \tag{34}$$

As given in the literature[2,7] of induction

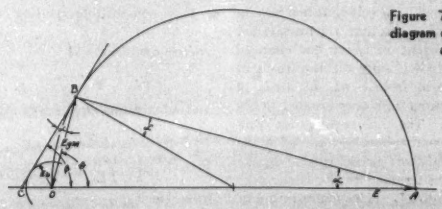


Figure 7. Circle diagram of grid voltage

heating

$$R = K_1 N^2 \rho^{1/2} \omega^{3/2} \mu^{1/2} \quad (35)$$

where

ρ = resistivity of the charge

μ = permeability of charge = 1 for molten condition

N = turns in inductor coil. (The term N^2 serves to convert the equivalent resistance of the charge to the reflected value in the coil circuit).

K_1 = a factor involving the dimensions of the charge and coil and the coupling between them.

Therefore

$$R = K\omega^{1/2} N^2 \quad (36)$$

for a given coil and charge.

$$R = \frac{K}{L^{1/4} C^{3/4}} N^2 \quad (37)$$

But

$$L = N^2 L_0$$

where L_0 is the inductance of a single-turn coil of the same dimensions.

Therefore

$$R = \frac{K N^{3/2}}{L_0^{1/4} C^{3/4}} \quad (38)$$

Substituting from equation 38 in equation 34

$$P = \frac{E^2 K C^{3/4}}{L_0^{5/4} N^{3/2}} \quad (39)$$

Therefore

$$P \propto \frac{C^{3/4}}{L_0^{5/4}} \times \frac{1}{N^{3/2}} \quad (40)$$

for constant E.

From equation 40 it is clear that taps in the inductor coil are not a very effective way of adjusting the power absorbed by the coil. The power is seen to vary inversely as the square root of the turns. This is true if the length and diameter of coil and charge are held constant so that L_0 is constant. In the actual case, where turns are tapped back from the ends of the coil a reduction in turns means a decrease in the length of the coil with a corresponding increase in L_0 and the power, P, will change even less than indicated by equation 40.

This is not the case for motor-generator set where the frequency is fixed. In that case, making the same simplifying assumptions as before,

$$I_L = \frac{E}{\omega L} = \frac{E}{L_0 N^2} \quad (41)$$

where

$$\omega = 2\pi f$$

$$R = K' N^2 \quad (42)$$

since ω in equation 36 is now constant.

$$P = I_L^2 R = \frac{K' E^2}{\omega^2 L_0^2 N^2} \quad (43)$$

Therefore,

$$P \propto \frac{1}{L_0^2 N^2} \quad (44)$$

for constant E and ω.

For equation 44, it is clear that in this case taps will be an effective means of adjusting power, although frequent switching of capacitors carrying heavy currents is required to keep the circuit tuned to the generator frequency.

It is of interest to make a similar comparison for operation of two furnaces in parallel from one high-frequency power supply.

Equations 39 and 43 can be rewritten substituting $L = L_0 N^2$ for the inductance of the load coil. Then for the self-excited converter

$$P = K E^2 N^2 \frac{C^{3/4}}{L^{5/4}} \quad (45)$$

and

$$f_1 = \frac{1}{2\pi C^{1/2} L^{1/2}} \quad (46)$$

For the motor-generator set

$$P = K' E^2 N^2 \frac{1}{\omega^2 L^2} \quad (47)$$

and in order to maintain unity power factor of the motor-generator circuit, it is essential that

$$C = \frac{1}{\omega^2 L} \quad (48)$$

Suppose that an installation consists of one high-frequency power source and two or more identical induction furnaces and that it is desirable to operate any one furnace alone or two furnaces in parallel. If the inductance of each furnace is L, then by equation 45 the power drawn by a furnace operating alone will be (for a self-excited converter):

$$P_1 = K E^2 N^2 \frac{C^{3/4}}{L^{5/4}} \quad (48a)$$

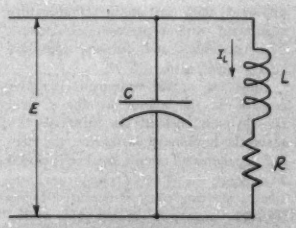


Figure 8. Equivalent high-frequency load circuit

Assume that P_1 is the allowable input to the charge. With two identical furnaces in parallel, each furnace can be considered as oscillating with one-half the total capacitor bank, C, so that the power, P_2, drawn by one furnace with two operating simultaneously is

$$P_2 = K E^2 N^2 \frac{\left(\frac{C}{2}\right)^{3/4}}{L^{5/4}} = 0.595\, K E^2 N^2 \frac{C^{3/4}}{L^{5/4}} \quad (49)$$

$$P_2 = 0.595 P_1 \quad (50)$$

The total power drawn by both furnaces will be

$$2P_2 = 1.19 P_1 \quad (51)$$

The total power has increased about 19 per cent. It will be necessary to reduce the voltage (by one of the methods described before) only about 10 per cent in order to bring the total power down to the rating of the converter.

The frequency will increase about 40 per cent as shown by equation 46.

$$f_2 = \frac{1}{2\pi \left(\frac{C}{2}\right)^{1/2} L^{1/2}} = \frac{\sqrt{2}}{2\pi C^{1/2} L^{1/2}} = \sqrt{2} f_1 \quad (52)$$

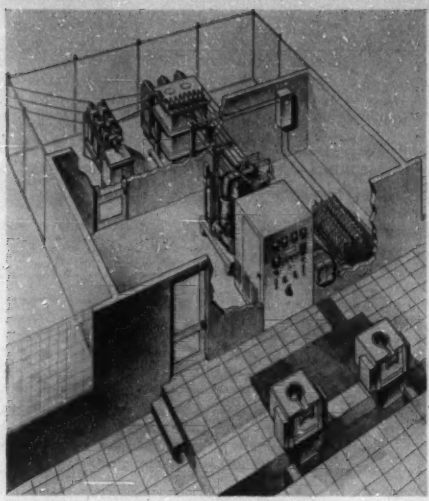
Looking at equation 47, for a motor-generator set, the power for one furnace alone is

$$P_1 = K' E^2 N^2 \frac{1}{\omega^2 L^2} \quad (53)$$

and for two furnaces in parallel the total inductance will be $L/2$ and it will be necessary as shown by equation 48 to double C in order to bring the circuit back to unity power factor at the generator frequency. Then the power drawn by each furnace alone will be the same as before, and, in order to bring the total power down to the rating of the machine, the voltage, E, must be reduced to approximately 70 per cent of the original value or the power to each furnace must be cut in half by inserting extra turns in the inductor coils.

Efficiency

The conversion efficiency is usually specified as that from the three-phase, a-c input line terminals to the single-phase a-c output transformer terminals. The principal losses in this circuit are the copper and core losses in the transformer elements, the arc drop loss in the mercury-arc converter, and the auxiliary power requirements for the converter unit. The copper and core losses at full load average from five per cent to seven per cent, dependent upon design considerations. The arc drop loss is of the



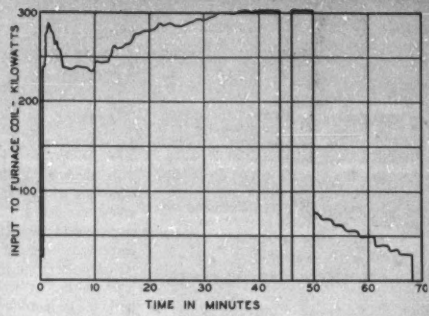


Figure 9 (left). Typical arrangement of induction melting equipment

Figure 10 (above). Typical chart of power input to melting furnace

order of 1.5 per cent, and the auxiliary power needed is about 1.5 per cent. Thus, the conversion efficiency at full load is better than 90 per cent. The efficiency curve at lighter loads has the same type of characteristic as that for mercury-arc rectifier equipment, remaining relatively flat at better than 90 per cent from full load to about one-quarter load.

The over-all efficiency obtained in converting alternating electric power into heat induced into the charge is dependent upon additional losses in the capacitor and heating-coil circuit. The loss in the capacitors is generally of the order of three per cent and is dissipated by water cooling. The loss in the heating coil is dependent upon design and operating conditions, and varies considerably with different types of applications and in heating different types of metals. Under the best conditions, the heating-coil efficiency may be as high as 85 per cent and in general it is better than 75 per cent for steel melting. The over-all efficiency therefore will be in the range of 65 to 80 per cent.

Description of Equipment

Installations of 300-kw and 600-kw mercury-arc frequency-changing equipment have been made in several foundries in the United States for induction melting of approximately 1,000 to 2,000 pounds of alloy steels and high-grade nonferrous alloys per hour. Furnaces range in capacity from 200 pounds to one ton.

A typical installation of 300-kw equipment, as shown in Figure 9, consists of a circuit breaker for connection to the incoming three-phase power line, a transformer consisting of three separate transformer elements assembled within a single oil-filled case, the mercury-arc converter unit, a control cubicle, a capacitor bank, and usually two or three furnaces with inclosed disconnecting switches. The circuit breaker and transformer are installed outdoors adjacent to the building or indoors within the substation containing the other electrical equipment. The furnaces are located in the melting department in the main foundry building.

The transformer is equipped with radiators and is self-cooled. The mercury-arc converter is water-cooled, and is provided with automatic temperature regulation and protection equipment. The capacitors and furnace coils are also water-cooled.

Operation of the equipment is under control of the melter at the control cubicle panel behind the furnaces. To place the equipment in service, the electrically operated circuit breaker is closed to energize the 60-cycle transformer, and then a starting relay is operated to release the negatively biased grids of the mercury-arc converter unit so that it will start oscillating.

In melting service, voltage control is of importance during the melt-down period in order to maintain automatically as close to maximum rated power as possible. The type of metal (magnetic or nonmagnetic), the size of pieces, compactness of charging, the thickness of the lining, and other factors all have a bearing on the amount of power induced into the charge in the furnace. A small automatic regulator functioning in the high-frequency grid phase-shifting circuit has been used to maintain constant voltage on the furnace coil. More recently a static-type grid-control circuit has been devised for several installations so as to make them inherently self-regulating with respect to the high-frequency output voltage.

At the conclusion of a heat, when the furnace charge is completely molten and raised to the desired temperature, power is removed by interrupting the alternating high-frequency voltage fed back to the grids of the mercury-arc converter. After deoxidation of the molten metal, power can be reapplied at one-half voltage by means of a tap changer on the high-frequency transformer or by 60-cycle phase control of the converter grids. This provides about one-quarter power which is sufficient to offset conduction and radiation heat losses and thus maintain the molten metal at a relatively constant temperature until the complete charge has been poured. A typical graphic chart of the kilowatt input to the furnace during melting of a charge and the batch pouring of molten metal is shown in Figure 10.

Installations of mercury-arc frequency-changing equipment can be designed for connection to any available power-line voltage. The inherent self-tuning characteristic results in simple operation with relatively little attention required from the furnace operator.

Appendix

C = capacitance of tuned load circuit
C_g = capacitance of grid phase-shifting circuit
E = effective value of high-frequency load-circuit voltage
E_b = direct bias voltage applied to grids
E_d = direct voltage under load
E_{d0} = direct voltage at no-load
E_d' = equivalent direct voltage including drop due to high-frequency overlap
E_g = effective value of alternating component of high-frequency grid voltage
E_{gm} = peak value of E_g
E_M = effective value of high-frequency transformer-primary voltage from center tap to one terminal
E_{M0} = E_M at no-load
E_m = peak value of E_M
E_r = drop in direct voltage due to transformer and reactor copper losses
E_s = effective value of no-load phase-to-neutral voltage of 60-cycle transformer secondary
E_{s1} = drop in direct voltage due to 60-cycle overlap
E_{s2} = drop in direct voltage due to high-frequency overlap
e_{a0} = instantaneous voltage from center tap of high-frequency transformer to anode 1
e_{a2} = instantaneous voltage from center tap of high-frequency transformer to anode 2
e_{k1} = instantaneous voltage from anode 1 to cathode
e_{k2} = instantaneous voltage from anode 2 to cathode
f = frequency of output voltage
f_1 = output frequency—one furnace operating alone
f_2 = output frequency—two furnaces in parallel
I_c = effective value of extrapolated commutating current = E_M/X_{cl}
I_{cm} = peak value of I_c
I_d = cathode current
I_L = effective value of heating-coil current
L = inductance of tuned load circuit
L_{cl} = high-frequency transformer commutating inductance in primary terms
L_e = equivalent inductance of single-turn coil having same dimensions as heating coil
N = heating coil turns
P = power input to charge to be heated
P_1 = power to charge—one furnace operating alone
P_2 = power input to one charge when two furnaces are operating in parallel
P_r = total copper losses of 60-cycle transformer, high-frequency transformer, and cathode reactor
$Q = L/R$ of heating coil
R = equivalent resistance of charge as seen from coil terminals
R_g = resistance in grid phase-shifting circuit
r = equivalent of direct voltage drop due to copper losses
u = angle of high-frequency overlap
X_{cl} = 60-cycle transformer commutating reactance in secondary terms
X_{cl} = high-frequency commutating reactance in primary terms
X_g = capacitive reactance in grid phase-shifting circuit
α = displacement of firing angle due to bias voltage
β = angle of advance, or firing angle
ϵ = base of Naperian logarithms
θ = phase displacement in grid phase-shifting circuit between E_g and E
μ = magnetic permeability of charge
ρ = electrical resistivity of charge
τ = time constant of damped sine wave of output voltage
ϕ = margin angle = $(\beta - u)$
ω = natural angular frequency of tuned load circuit angular operating frequency
ω_o = angular frequency of commutating voltage and current

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No Discussion

Aging Characteristics of Dry-Type Transformer Insulation at High Temperature

H. C. STEWART
MEMBER AIEE

L. C. WHITMAN
MEMBER AIEE

INSULATIONS used in class B insulated dry-type transformers consist of such materials as glass yarn and sheet asbestos and even cellulose in combination with a heat-resistant varnish. The life characteristics of such insulation with particular reference to the gas medium in which the material is required to function is discussed in this paper. Further data on the aging of this insulation will be available when the co-operative aging tests that are being conducted under the supervision of the working group of the AIEE transformer committee are completed.

The life of any material is dependent not only upon its temperature but also upon the medium in which that temperature is attained. There is a fundamental difference in the life of insulation containing organic materials operating in an insulating liquid or in a gas medium. In the former instance, the material ages mechanically, but by virtue of liquid absorption, it maintains its electrical strength to a remarkable degree if undisturbed by vibration. In the latter case where the material operates in a gas medium, there is not only a reduction in the mechanical strength but in the electrical strength as well. This latter situation results from the aging of the varnish impregnant used to impart high dielectric and mechanical qualities to the base material. In the process of deterioration, the varnish is replaced by air which has a much lower dielectric strength.

Engineers are conscious of the fact that the reduction in dielectric strength of electric apparatus can be serious since it encroaches upon the factors of safety originally designed into such equipment. Such loss renders the apparatus less likely to withstand the more severe operating conditions, such as lightning, switching surges, and short circuits.

Recognition of these conditions leads to the conclusion that dielectric strength is a dominant factor in evaluating the life of insulations aged in a gas medium. On this premise, a percentage of the initial dielectric strength, at operating temperature, has been chosen as the criterion. The value of this percentage selected as indicative of insulation life is arbitrary. An approach to 50 per cent reduction is increasingly dangerous and may well represent the end of life. The major part of these data, therefore, is predicated on the end of life occurring at a point where the dielectric strength has diminished to one-half of its initial value at operating temperature.

Data are presented on both class A and class B insulations subjected to temperatures in excess of presently-established operating levels. From these data, extrapolation can be made to indicate anticipated life at normal operating temperature. The results shown here indicate the maximum life which can be expected from insulations when maintained continuously at a given operating temperature. Intermittent operation may introduce other conditions, such as moisture absorption, contamination of the operating medium, and so forth, which will be a factor in further reducing the life beyond that indicated by these data.

Finally, these data and arbitrarily assumed load cycles are used to estimate the life of a dry-type transformer.

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H. C. STEWART and L. C. WHITMAN are both with the General Electric Company, Pittsfield, Mass.

Material Investigated

The varnish impregnant used is a heat-resisting type and is specifically referred to hereafter as a high-temperature phenolic varnish.

The following list is a specification of the materials tested.

1. Commercial grade of sheet asbestos.
   Thickness—15 mils.
   Impregnant—high-temperature phenolic varnish.
   Cooling medium—air at atmospheric pressure.
2. Commercial grade of sheet asbestos.
   Thickness—15 mils.
   Impregnant—high-temperature phenolic varnish.
   Cooling medium—nitrogen at 5 pounds gauge pressure.
3. Fiber glass yarn.
   Thickness—20 mils.
   Impregnant—high-temperature phenolic varnish.
   Cooling medium—air at atmospheric pressure.
4. Electrical grade of black-varnished cambric.
   Thickness—7 mils.
   Impregnant—no additional.
   Cooling medium—air at atmospheric pressure.
5. Electrical grade of kraft paper.
   (Grade used in insulating cylinder)
   Thickness—4 mils.
   Impregnant—high-temperature phenolic varnish.
   Cooling medium—air at atmospheric pressure.

The curing time and temperature for the sheet-asbestos and glass-yarn test pieces are those recommended by the manufacturer for curing the high-temperature phenolic varnish used. In the case of the kraft-paper test pieces, the curing was accomplished by a reduction of temperature with an increase in duration of cure to prevent deterioration of this class A material.

Figure 1. Component parts of insulation life test piece

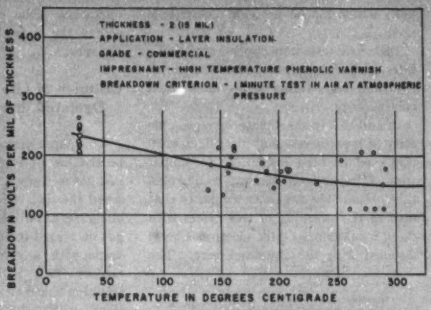


Figure 4. Initial dielectric strength asbestos sheets

These are representative of some of the class A and class B transformer insulations used as winding layer material, wire covering, and insulating cylinders. While only a few of the possible combinations were tested, they are believed to be a fair representation of the insulations used in dry-type transformers and other air-insulated apparatus.

Choice of Test Pieces

The physical arrangement of the test material was selected to satisfy the following conditions:

1. Use of material in customary form.
2. Convenient and accurate measurement and control of the temperature of the test piece.
3. Provision for applying the dielectric test to the entire test piece.
4. Temperature gradients in test piece comparable to those encountered in practice.
5. Inclusion of oxidation effects on the test piece by exposure to normal ambient conditions.
6. Visual inspection of test piece for check on mechanical effects, such as crazing or cracking.
7. A structure that could be treated and handled as a unit without danger of damage to the insulation.

These requirements are realized in a practical sense in the test piece used and described in the following.

Details of Test Piece and Testing Technique

The heat source consisted of a 2-inch diameter metal tube housing a calrod-type of heating unit, Figure 1. Uniform heat conduction from the heating unit to the tube was facilitated by a magnesium oxide filling.

Temperatures of the inner surface of the insulation were obtained from thermocouples brazed at intervals along the tube.

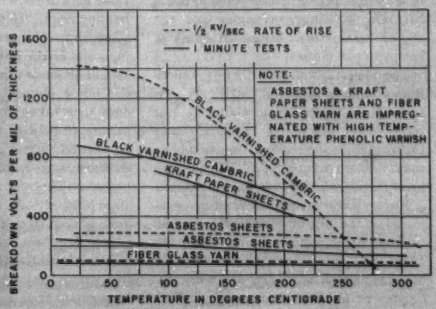
Temperatures of the outer surface of the insulation were measured with thermocouples brazed to the narrow external electrodes which cover the insulation areas under test, Figure 2. Outer thermocouples, usually greater in number, were located directly over the tube thermocouples, so that it was possible to plot temperature gradients parallel with the inner and the outer surfaces. Precautions were taken in every instance to see that thermocouple leads did not conduct heat from the thermocouple junction, thereby affecting the temperature accuracy. This was accomplished by associating at least six inches of the thermocouple leads with the junction temperature. The complete assemblies were supported horizontally with thermocouples along the upper, or "12 o'clock," surface. Figures 1 and 2 show partial and complete assemblies of the test piece.

The number of turns of insulation wound on the tube was determined by the dielectric strength of the material. Insulation thickness was selected so that the voltage was sufficient to minimize the erratic breakdown behavior of small spacings. Usually this resulted in a minimum of 2,000 volts and a maximum of 4,000

Figure 2. Partial and complete assembly of insulation life test piece

Figure 3 (below). Insulation life test piece using glass-covered wire

Figure 5. Summary of initial dielectric strength in air at atmospheric pressure



volts where the 50 per cent criterion is used.

Dielectric tests were applied at periodic intervals between the tube and each outer electrode with temperature maintained continuously.

In addition to sheet material representative of layer insulation, glass-fiber yarn was used as turn insulation on copper wire. The insulated wire was wound on a tube prepared in a manner similar to that used in the assembly of test pieces with sheet insulation. Outer electrodes were fastened over the insulated wire. The

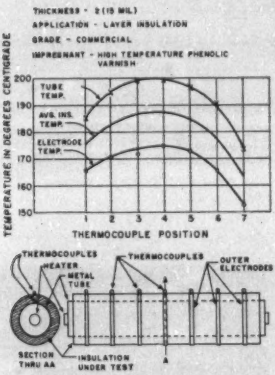


Figure 6. Insulation temperature gradients in air at atmospheric pressure—asbestos sheets

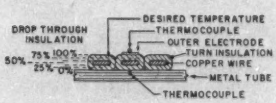


Figure 7. Thermal drops through fiber glass yarn on copper wire

copper conductor was grounded to the tube so that dielectric tests could be made between the outer electrodes and the copper conductor. Figure 3 shows a complete test piece.

The sheet asbestos and kraft papers were preimpregnated but cured only to a tacky state before being wound on the tube. After the outer electrodes and thermocouples were added, the assemblies were vacuum treated with a high-temperature phenolic varnish and fully cured.

Sheet black-varnished cambric was placed on the tube in its original form and received no subsequent impregnation.

The fiber glass-yarn turn insulation had included varnish which acts as a lubricant to prevent abrasion during the winding operation. The complete assembly received the varnish impregnation under vacuum.

Criterion of Life of Insulation Operated in a Gas

This paper proposes that the end of expected life of insulation when operated in a gas medium be considered as reached when the dielectric strength (referred to on figures as initial hot dielectric strength) has decreased to 50 per cent of the initial value with a given temperature maintained. This is an entirely arbitrary selection based on such general considerations as initial insulation level of air-insulated apparatus; exposure of this apparatus to transient voltages as affected by usual location and application; and of allowance of the decrease of factor of safety to a dangerously low value. As a matter of general interest, some data are also given for decrease of dielectric strength to 75 per cent of initial strength.

Determination of Initial Dielectric Strength at Test Temperature

With the insulations tested it was first necessary to determine the initial dielectric strength as a function of temperature, that is, the dielectric breakdown of the insulation the first time it was brought up to the testing temperature. Most of these data are based on 60-cycle 1-minute holding tests. However, there might be some controversy as to whether the determination of the initial dielectric strength should be made using a 1-minute holding test or a momentary test. Therefore, data were taken on both bases. The 1-minute tests were made starting at approximately 60 per cent of the anticipated strength and holding voltage for one minute. This voltage was increased in approximately 10 per cent increments, holding each step one minute until failure occurred. This maximum voltage at failure is considered to be the initial dielectric strength at operating temperature. Momentary tests were made using a rate rise of 1/2 kv per second as recommended by American Society for Testing Materials Standards.[1] All initial breakdown tests were reduced to a volts-per-mil basis, using the average of ten measured thicknesses for each test piece.

Method of measuring thickness of insulation is described in American Society for Testing Materials Standards[2] except that in case of turn insulation, standard machinist's micrometers with 1/4-inch diameter anvils were used.

Typical data for asbestos are shown on Figure 4. Composite curve, Figure 5, covers all material tested.

The possible advantage of the momentary test is that corona is minimized. The disadvantage is that it is not as searching a test as the 1-minute test and does not give opportunity for thermal failure which may be encountered in practice. For many years, the 1-minute holding test has been the standard for factory acceptance tests. This factors the thermal effect particularly important in class B asbestos insulations operated in air.

Table I

Watts Per Sq.in.	Number of Layers of Sheet Asbestos Impregnated with High-Temperature Phenolic Varnish: Mandrel Temp. °C	2 Layers (0.0352) °C Drop	2 Layers (0.0352) °C Mil	3 Layers (0.0525) °C Drop	3 Layers (0.0525) °C Mil	4 Layers (0.0692) °C Drop	4 Layers (0.0692) °C Mil	Avg. °C Per mil	°C Per Watt Per in.² Per in.
1.25	160	11.5	0.327	17.0	0.324	24.0	0.345	0.332	.262
1.85	200	15.6	0.443	23.0	0.438	32.0	0.463	0.448	.243
2.45	240	20.5	0.582	29.8	0.568	40.8	0.588	0.579	.236

Determination of the Criterion Voltage for Breakdown of Insulation Life Pieces

Knowing the average insulation thickness, the initial breakdown voltage was calculated from Figure 5. Then the breakdown test voltage was taken as 25, 50, or 75 per cent of the initial breakdown voltage, depending upon which figure was chosen as the criterion of life. Both the 1-minute and momentary tests were made at time intervals of approximately two per cent of the anticipated life of the insulation. For the momentary tests, the voltage was increased at the rate of rise of 1/2 kv per second and, on arrival at desired level, was reduced at the same rate.

Temperature Determination

A complete measurement of all thermocouples was made as soon as temperature

Figure 8 (left). Chamber for testing insulation pieces in inert atmosphere

equilibrium had been attained on each test piece. These were plotted then as shown on typical curves in Figure 6. The average insulation temperatures, existing for each test point, were calculated by averaging the corresponding reading from the maximum to minimum temperature curves.

It will be noted that considerable radial gradients are indicated from the thermocouple in the tube wall to the thermocouple on the outer electrode. The point might be raised whether these gradients are actually in the insulation itself or whether they might be largely in a theoretical air film each side of the insulation. To investigate this, several test pieces were made up with different numbers of layers of insulation. The watts input to the heating unit was measured and the temperature determined.

From these data, the degree temperature drop-per-mil insulation thickness was calculated, Table I. It is seen that these values are approximately the same without regard to number of layers, showing that gradients are in the insulation itself and only small surface film drops exist. Also, temperature resistivities of approximately 250 degrees centigrade per watt per square inch per inch, for impregnated asbestos were determined which approximately check the values of similar insulation.[3]

There remains the circumferential temperature gradients on these test pieces. Table II shows result of tests with thermocouples placed around tube and outer electrodes. It is seen that these differences are small; the maximum temperature on the bottom being only about 2.5 per cent more than that on the top. In examining pieces that failed, records were kept as to the position of failure. No particular pattern or trend was noted in these records.

In the case of the fiber glass wire pieces, the average insulation temperature was determined by taking 25 per cent of the difference between the tube temperature and the outer electrode temperature and adding this 25 per cent to the outer electrode temperature. That this is a proper procedure will be clear from Figure 7 and the following considerations.

The temperature gradients here can be considered in three parts:

1. Gradient from the metal tube to the copper through the insulation between the tube and the copper.
2. Gradient through the copper wire itself.
3. Gradient from the copper wire to the outer electrode through the insulation between the copper and the outer electrode.

It is at once evident that items 1 and 3 are equal in magnitude from symmetry, neglecting small differences due to curvature. Let us compare the thermal drop through item 2 compared with the sum of those through items 1 and 3.

The thermal resistivity of copper is 0.1125 degree centigrade per watt per cubic inch, while that of fiber glass is approximately 250 degrees centigrade per watt per square inch per inch.

The thermal drop through the copper can be neglected since its thermal resistance is roughly 1/1,000 of that through the fiber glass for the thicknesses involved. Since we are applying the dielectric test between the outer electrode and the wire, we are interested in the average temperature of the outer portion only; that is, that of item 3. Again from symmetry, the average temperature of this region is the temperature of a point halfway from

Table II. Temperature on Tube Under Insulation

Run Number	12 O'Clock Position Number 1	3 O'Clock Position Number 2	6 O'Clock Position Number 3	9 O'Clock Position Number 4
1	155.2	158.0	159.0	156.7
2	185.3	187.1	188.1	185.9
3	232.2	236.1	237.9	234.4

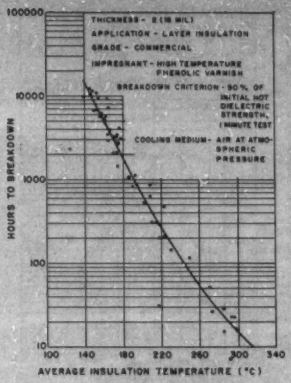


Figure 10. Life characteristics—asbestos sheets

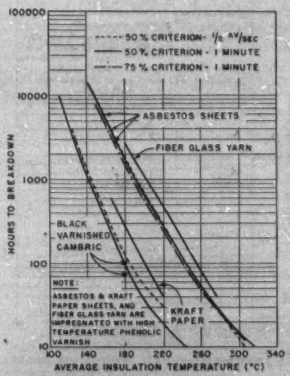


Figure 9. Summary of life characteristics of insulations in air at atmospheric pressure

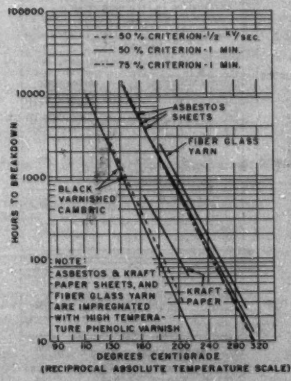


Figure 11. Summary of life characteristics of insulations in air at atmospheric pressure

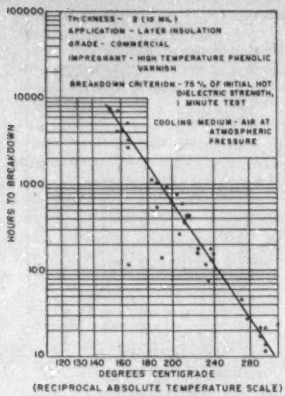


Figure 12. Life characteristics—asbestos sheets

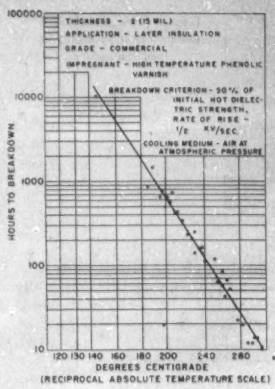


Figure 14. Life characteristics—asbestos sheets

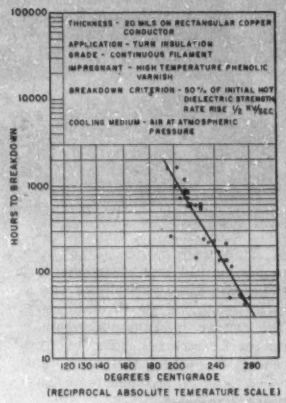


Figure 16. Life characteristics—fiber glass yarn

the copper wire to the outer electrode, which is 25 per cent of the effective thermal distance and hence 25 per cent of the temperature drop from outer electrode to tube surface. Again, we are neglecting the correction for curvature, which would be negligible.

The voltage supply to the heating unit in all cases was maintained at a uniform value by means of voltage stabilizer units so that heating was essentially constant. The ambient temperature was closely maintained by auxiliary heat and exhaust fans, both thermostatically controlled. The variation of the temperature of the insulation under test was not more than five degrees from the average value over any 24-hour period, and for practically all the time the variation was less than two degrees.

Tests Made in Inert Gas

Most of the data in this paper were obtained with the samples exposed to free air at atmospheric pressure.

In order to see the effect of oxygen on the rate of deterioration, some units were sealed in a chamber in pure nitrogen. The units were constructed in the same manner as all others and electrodes brought out of the sealed chamber for dielectric tests and for supplying heating current.

Temperatures of all parts of the unit were determined by thermocouples as in previous tests. The gas pressure was set at five pounds gauge to insure the purity of the nitrogen gas. Also, this would be a possible operating condition for a sealed dry-type transformer. Construction of this chamber is shown in Figure 8.

Results to date indicate that the life of impregnated asbestos operated in this manner is considerably lengthened, at least for temperatures above 200 degrees centigrade. No failures in the insulation

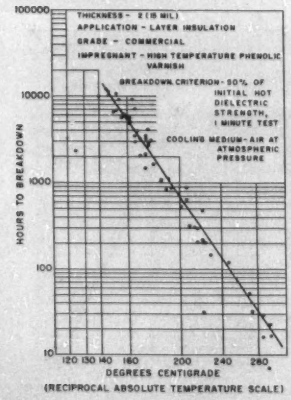


Figure 13. Life characteristics—asbestos sheets

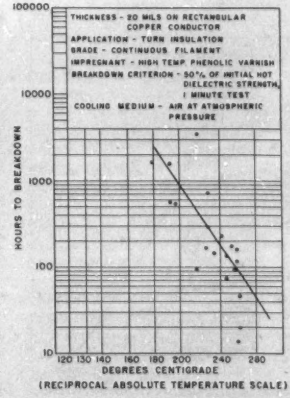


Figure 15. Life characteristics—fiber glass yarn

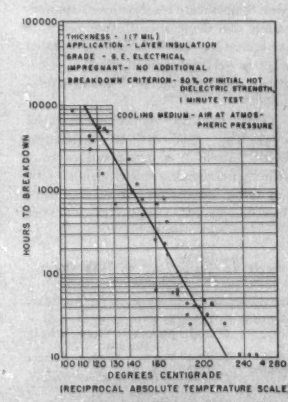


Figure 17. Life characteristics—black varnished cambric sheets

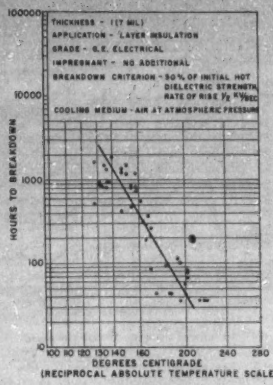


Figure 18. Life characteristics—black varnished cambric sheets

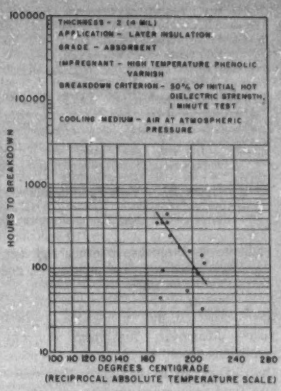


Figure 19. Life characteristics—kraft paper sheets

have yet occurred in the elapsed testing time which is roughly three times as long as that for similar insulation operating in free air at the same temperature. This would seem to allow an increase of at least 25 degrees centigrade in hot-spot temperature for class B insulation, operated in an inert atmosphere, with normal life. Whether the relation holds for lower temperatures is not yet known as not enough data are available to determine the "degree rule." This relation may or may not hold for other insulations, although some improvement is probable.

Results

1. Figures 9 to 19 inclusive, give the life characteristics of the various insulations investigated in this research. It is to be noted that in all cases these are plotted with the hours to breakdown on the logarithmic y axis, but that the average insulation temperature is plotted on the uniform x axis with an evenly divided degree-centigrade scale, Figures 9 and 10; and also with a reciprocal absolute temperature scale,[4] Figures 11 to 19 inclusive. The latter gave straight-line characteristics, allowing greater ease in extrapolation.

2. Summary of all life characteristics is given in Figure 9 and Figure 11 plotted as a function of temperature in degrees centigrade, and reciprocal absolute temperature degrees centigrade, respectively.

3. Extrapolation of these data on class B material to 130 degrees centigrade continuous operation shows that dielectric strength is reduced to one-half of its initial value in 3 to $4^1/_2$ years, where insulations operate in a gas medium, see Table III.

4. Extrapolation of these data on class A material to 105 degrees centigrade continuous operation shows that dielectric strength is reduced to one-half of its initial value in approximately two years, where insulations operate in a gas medium, see Table III.

5. The so-called degree rule (degrees increase in temperature to reduce life 50 per cent) of insulations operated in a gas medium changes markedly depending upon the class of insulation and its operating temperature.

6. Degree rule for class A insulations[5] in a gas medium is indicated as halving the insulation life for each approximately 10 degrees centigrade increase in the range of 105 to 150 degrees centigrade, Figure 20.

7. Degree rule for class B insulations in a gas medium is indicated as halving the life for each approximately 13 degree centigrade increase in the range of 130 to 200 degrees centigrade, Figure 20.

8. It is interesting to note that in general, the life of class B materials is ten times that of class A materials operated in a gas medium at the same temperature, see Figures 9 and 11.

9. If a life criterion at 75 per cent of initial strength is used instead of 50 per cent, a further shortening of life of about 10 per cent will result, see Table III.

Evaluation of Data in Terms of Dry-Type Transformer Life

Extrapolation to 130 degrees centigrade as shown in Table III indicates that the initial dielectric strength is decreased to half value for class B insulations in 3 to $4^1/_2$ years of continuous operation. The variation in years is due to the individual material and the method of extrapolation employed. A value of 30,000 hours (3.4 years) is obtained considering asbestos and the inverse absolute temperature[6] extrapolation, which appears to be the more accurate method.

Dry-type transformers are seldom loaded to capacity for more than 8 out of 24 hours and so do not operate at ultimate temperature for more than about 5 out of 24 hours, due to inherent thermal capacity. Since these data are obtained on the basis of continuous operation, it is, therefore, necessary to introduce a factor which evaluates these data in terms of life where a transformer does not operate at maximum temperature continuously. Based on the foregoing assumption, dry-type transformer life in terms of these data can be assumed to be increased 24/5 or 4.8 times. This is equivalent to saying that a dry-type transformer operating on this load cycle and attaining a hot-spot temperature of 130 degrees centigrade will

Table III. Life of Material Operated in Air at Atmospheric Pressure

Breakdown Voltage Used as Life Criterion

	Initial Dielectric Strength Breakdown Criterion, Per Cent	Using Reciprocal of Absolute Temperature Scale			Using Average Insulation Temperature Scale		
Type of Voltage Test		1 Minute Holding	1/2 kv Per sec Rate		1 Minute Holding	1/2 kv Per sec Rate	
Duration of Test		Hours	Hours	Years	Hours	Hours	Years
Materials							
Varnished cambric at 105°C	50	18,500	19,000	2.1	16,000	16,500	1.8
Kraft paper at 105°C	50	18,500		2.1	11,500		1.3
Asbestos at 130°C	50	30,000	31,000	3.4	26,000	25,000	2.9
Asbestos at 130°C	75	27,000		3.1	21,000		2.4
Glass yarn at 130°C	50	40,000		4.5	25,000		2.8

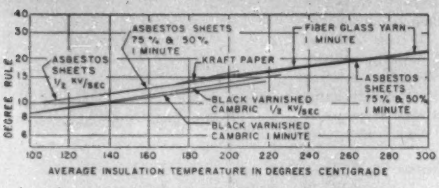


Figure 20. Variance of degree rule with temperature

Insulation in air at atmospheric pressure (Derived from Figures 12 to 19 inclusive)

Asbestos and kraft paper sheets, and fiber glass yarns are impregnated with high temperature phenolic varnish

have expected life of $(4.8 \times 3.4) = 16.3$ years. Moreover, maximum ambient conditions would seldom exist for more than 50 per cent of the time. This would also tend to increase the life expectancy since insulation deteriorates at a lesser rate at lower temperatures. A 10 degree reduction in ambient would result in a hot-spot temperature of 120 degrees centigrade. From Figure 13, life at 120 degree centigrade is 57,000 hours (6.5 years) for continuous operation. For the load cycle of 8 hours (5 hours effective) operation out of 24 hours, the expected life at 120 degrees centigrade becomes $24/5 \times 6.5$ years $=$ 31.2 years. Then, assuming that the transformer operates one-half the time at the lower ambient, which results in 120 degrees centigrade temperature, and the remainder of the time at 10 degree higher ambient, resulting in 130 degrees centigrade temperature, the expected life becomes

$$\frac{1}{1/2(1/16.3)+1/2(1/31.2)} = 21.4 \text{ years}$$

This is a reasonable situation and parallels experience in the field with liquid-filled transformers. It is interesting to note that if this same class B dry-type transformer is designed for a 160 degrees centigrade hot-spot temperature with the same 10 degrees centigrade ambient fluctuation, its life for continuous operation from Figure 13 is as follows:

150 degrees centigrade—9,200 hours—1.05 years.

160 degrees centigrade—5,200 hours—0.59 years.

For the condition where the transformer operates one-half at each temperature and for the previously assumed loading cycle, the expected life is

$$\frac{24}{5}\left[\frac{1}{1/2\left(\frac{1}{1.05}\right)+1/2\left(\frac{1}{0.59}\right)}\right] = 3.63 \text{ years}$$

This is one-sixth of the estimated life at 130 degrees centigrade.

The deterioration of class B insulation for overload conditions for various periods has been calculated on the basis of the following criteria and is shown in Figure 21.

1. End of life as 3.4 years when operated continuously at 130 degrees centigrade.

2. Deterioration rate, incremental degrees to halve life, at temperatures in excess of 130 degrees centigrade as on Figure 20.

This is a useful guide to assess the injury to insulation operated at a temperature in excess of 130 degrees centigrade.

Data from Table III and Figure 11 indicate a 30 per cent or 5-degree advantage for glass yarn over commercial grades of asbestos. This is undoubtedly due to contaminants in the asbestos and can be circumvented by the use of refined grades of asbestos or Terratex.[6]

Conclusions

1. A new method of determining the life characteristic of insulations operated in gaseous mediums has been developed. In this method, failure voltage at operating temperature expressed as a percentage of the initial breakdown voltage has been used as a criterion of the length of life. The results are self-consistent and constitute a valuable and accurate tool, yielding results in terms of voltage strength which is of major importance in electric apparatus.

2. In voltage testing to breakdown, the question of effect of corona on life has been raised where tests are of 1-minute duration. In this investigation, the results in terms of hours of operation are substantially the same for either momentary or 1-minute applications of voltage.

3. These data support the chemical deterioration theory[4] that the logarithm of time to reach a particular percentage of the dielectric strength of insulation is

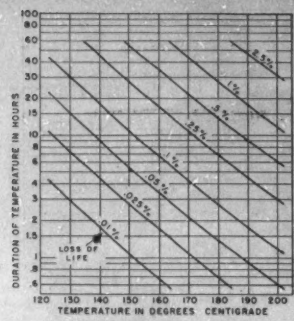


Figure 21. Loss of life in air—asbestos sheets impregnated with high-temperature phenolic varnish

proportional to the reciprocal of the absolute temperature.

4. Based on our data, it appears unwise to consider continuous operating hot-spot temperatures in excess of 130 degrees centigrade for class B insulations in use at the present time.

5. Considering the normal operating temperatures encountered by dry-type transformers due to normal load cycles and ambient fluctuations, it has been shown that such a transformer operated at not over 130 degrees centigrade hot-spot temperature will have a life comparable with the normal liquid-filled transformer.

References

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5. Temperature-Aging Tests on Class-A-Insulated Fractional-Horsepower Motor Stators, J. A. Scott, B. H. Thompson. Electrical Engineering (AIEE Transactions), volume 61, July 1942, pages 499–501.

6. Terratex, a Thin Flexible Inorganic Insulation, T. R. Walters. AIEE Transactions, volume 67, part I, 1948, pages 133–7.

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Discussion

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): When a new insulating material is introduced on large-scale production there are two general methods open for the selection of the maximum or limiting hottest spot temperature of the insulation. One method is to select a temperature limit based on available laboratory data, which may be exceedingly limited, and use this limit in designing transformers, hoping that several years experience will show that the temperature limit is safe. The second method is to base the temperature limit on the result of extensive laboratory aging tests on the insulating material.

The experience method of determining the limiting insulation temperature for a reasonable length of time is exceedingly difficult of application, and requires a long time—20 years or more. Carefully kept records of loading and ambient temperatures covering many installations are needed as well as an exceedingly high degree of skill and ability in the analysis of the records.

The laboratory test method is also difficult of application. It requires that some characteristic of the insulation, mechanical or electrical, be selected as the basis of comparison of the effects of various temperatures and time. It also requires that some arbitrary value of the selected characteristic, in relation to its initial value, be chosen as the end point of the useful life of the insulation. It needs, too, the exercise of sound engineering judgment tempered by experience to translate the test results into design limits for the industry.

The use of class B insulation is, of course, not new. AIEE Standards have defined class B insulation and for many years the maximum temperature has been set at 130 degrees centigrade. Class B insulation has been used in rotating machinery for many years, but its use in moderate sized dry-type power transformers started at a fairly recent date, some eight or ten years ago.

The question of whether 130 degrees centigrade hot-spot temperature was the correct value for dry-type transformers was debated for several years in the AIEE transformer subcommittee (now the transformer committee) without getting anywhere. To settle this question it was decided by the transformer committee to start some co-operative aging tests in various laboratories. A working group, who had carried out co-operative aging of class A insulation, was requested to undertake this job of making laboratory tests on classes B and H insulation. Today four laboratories, General Electric, Westinghouse, Allis-Chalmers, and Dow-Corning, are co-operating in the making of these tests.

As previously stated, the two available methods for determining the maximum temperature limit for a new insulation are laboratory tests and experience in the operation of transformers. To obtain the final answer for classes B and H insulations will probably require results obtained by both methods.

The very complete data presented in the paper by Stewart and Whitman are, of course, in the laboratory class. These tests were started approximately $1^1/_2$ years before the AIEE working group was requested to start its co-operative aging tests on classes B and H insulations.

I feel that exhaustive laboratory tests, similar to these tests or to the test that will be finally agreed upon by the working group, are absolutely necessary. The authors are to be highly commended for presenting the very complete data in their paper. The results should serve as a "bench mark" for many years to come.

As a final check on their work, and on the work that has been carried on in the Westinghouse Laboratory at East Pittsburgh, the working group is planning on making further pilot tests to decide on a final method for evaluating the effect of temperature on these insulations. When this point is settled the main program of tests will be carried out.

T. R. Walters (General Electric Company, Pittsfield, Mass.): The authors have presented an interesting addition to the relatively meager literature on aging characteristics of class B insulations.

For the past several years we have been carrying out, in the laboratory at Pittsfield, aging tests on various class B insulations independently of the tests carried out by Stewart and Whitman. Our tests were carried out in circulating air ovens at temperatures of 150, 175, and 200 degrees centigrade. Reference to some of the earlier data at 175 degrees centigrade was given in a paper by T. R. Walters.[1]

Although various criteria have been used for determining the end of the useful life of insulation, we believe that one important factor in the life of dry-type apparatus is humidity. As long as the apparatus is in service, it will remain fairly dry. However, if it is shut down for weekends or other extended periods, the insulation depends on its varnish treatment for moisture protection. Accordingly, our laboratory tests were set up so as to periodically measure power factor of the samples after 24-hours exposure to 90 per cent relative humidity at 90 degrees Fahrenheit, as a check on the effect of high temperature aging. A "runaway" power factor condition at high humidity was taken as the end-point of insulation life.

The advantages of this method of judging end-point are that the break in the power factor versus time curve is relatively sharp; power factor values are not affected to any great extent by differences in the size of samples; and small imperfections in the samples are averaged out.

Comparing our data with that shown by Stewart and Whitman, we find that we are in general agreement, even though our tests were carried out quite independently, using a different test procedure and a different criteria for the end of useful life. The same insulations and high temperature varnish were used. The authors data indicate a "degree rule" of 13 degrees centigrade for this class of insulation. Our results showed a slightly higher "degree rule," 14 degrees centigrade or higher, with a somewhat shorter life at any given test temperature. The shorter life may be partly explained by the fact that our tests were run in an oven, where the samples were at a uniform temperature throughout, while the authors simulated service conditions where there was a temperature drop through the insulation.

In any event, our data tends to corroborate Stewart and Whitman's conclusion that 130 degrees centigrade is about the maximum hot spot temperature that can be permitted in dry-type class B insulated apparatus operated in free air, if the usual useful life is to be expected in service.

REFERENCE

1. See reference 6 of the paper.

H. C. Stewart and L. C. Whitman: T. R. Walters has indicated that humidity may have a bearing on the life of insulations operating in free air. We purposely omitted the humidity angle since we were interested in the maximum life of insulating materials. It is possible that apparatus may be operated continuously, either with or without load, in which case there will be sufficient heat to prevent condensation and water absorption. Under such conditions, humidity will have little effect on insulation life. Should there be heating and cooling periods due to shutdown, then humidity can be an unfavorable factor. Both conditions should be considered eventually, but it is more important to consider the most favorable condition when consideration is directed to the increase of the temperature limit for class B insulation. It is gratifying to find that the two methods result in general agreement as to life of this type of material. It would appear that dielectric tests are sufficiently searching and that the complication of humidity may not be justified. The particular advantage of the method we proposed lies in the fact that life is evaluated in terms of the original dielectric strength and so is easier to interpret than resistance and power-factor tests.

V. M. Montsinger has commented on the difficulties encountered in setting up temperature limits for insulation. We have attempted to show that the established 130 degrees centigrade hot-spot temperature should not be exceeded in dry-type transformers.

Further information may substantiate the use of higher temperatures for class B insulations in inert atmosphere than is permissible when they are exposed to the oxygen of air.

We hope that the data presented here will be of assistance to the AIEE working groups in their evaluation of the effect of temperature on class B insulation.

Measuring Transformer Winding Temperatures Continuously with the Transformer Carrying Load

A. M. LOCKIE
MEMBER AIEE

G. M. STEIN
MEMBER AIEE

Synopsis: Conventional test methods are not sufficiently accurate for the measurement of heating transients in transformer windings when high or rapidly changing temperatures are involved. For resistance measurements made after shutdown, the accuracy is increased by improved methods of evaluation which may be based on a cooling curve or on a single test point. These calculations are somewhat laborious, and are limited in accuracy and usage. For this reason, suitable connections are described for testing winding temperatures with d-c bridges throughout any load cycle, with or without the application of a-c line voltage. This technique applies to the factory test of single or polyphase transformers and will measure the temperatures in two or more windings simultaneously. Test results of temperature transients obtained by this new method are included in curve form.

PRESENT standards impose only one restriction on the temperatures in transformer windings, namely, that the average rise above ambient temperature shall not exceed a specified amount, usually 55 degrees centigrade, under continuous operation at rated load.[1] Consequently, the only temperature test, commonly made, demonstrates whether this requirement has been met. Under these conditions, the winding temperatures are low, and thermal equilibrium is reached before readings are taken, so that a resistance test, obtained within a reasonably short time after shutdown, usually gives sufficiently accurate results for commercial purposes.

Since the permissible winding rise has a fundamental effect on the economy of transformer construction, considerable effort is being made to increase that rise. However, when relatively high temperatures are involved, and particularly when these temperatures do not reach steady-state values, the conventional test procedures are not accurate enough. Such conditions are encountered in testing certain dry-type transformers at rated load, or in making overload tests in oil-immersed apparatus.

Use of New Insulating Materials

In the design of dry-type transformers, increasing use is made of materials which will withstand higher temperatures than cellulose. Some of these materials are allowed to reach temperatures of as much as 200 degrees centigrade for continuous operation.[2]

Performance on Heavy Overloads

Heavy overloads present a similar problem for all types of transformers. Such overloads are permissible, because the thermal capacity of the copper, oil and other parts of the transformer causes a time lag between load changes and temperature changes. Consequently, a winding which is not at the temperature limit for continuous operation may be overloaded for a short time without exceeding that limit.[3] Furthermore, higher temperatures may be permitted for short times, than for longer times, with no greater loss of life of the insulation.[4,5] For these reasons, a transformer may be selected for a given location so that the anticipated peak load will exceed the rating, thus utilizing the inherent short-time overload capacity.[6] This is particularly true in transformers with internal circuit breakers, intended to permit the greatest utilization of overload capacity consistent with satisfactory transformer life.

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A. M. Lockie and G. M. Stein are both with Westinghouse Electric Corporation, East Pittsburgh, Pa.

Typical Transformer Performance

To illustrate the nature of the quantities involved in testing at heavy overloads, consider the following data, obtained from tests on a small oil-insulated distribution transformer. Figure 1 illustrates the temperature variations which will occur if rated load is applied to a previously unexcited transformer, is continued until equilibrium is reached, and then is removed to allow a return to ambient temperature. The corresponding transients are plotted in Figure 2 for a heavy short-time overload of the same transformer, and are distinguished from those in Figure 1 by being interrupted long before the temperatures reach their equilibrium. It should be noted that the test results on Figure 2 and subsequent figures have been obtained solely to illustrate the considerations and methods presented in this paper, without regard to the effect of temperatures upon the transformer insulation.

Of particular interest in Figures 1 and 2 is the difference between copper and oil temperature; this quantity is independent of the ambient, and is determined essentially by the design of the winding only. The difference is often referred to as a "gradient," or as the average temperature rise of the copper above that of the oil. Since the word, gradient, is used commonly to indicate a rate of change of a potential, the temperature difference will in this paper be designated as a differential. Consequently, let

T = average rise in copper temperature above ambient at any time; degrees centigrade
θ = oil rise above ambient at any time; degrees centigrade
D = average copper differential at any time; degrees centigrade
T_F, θ_F, D_F = final or steady values of T, θ, and D; degrees centigrade

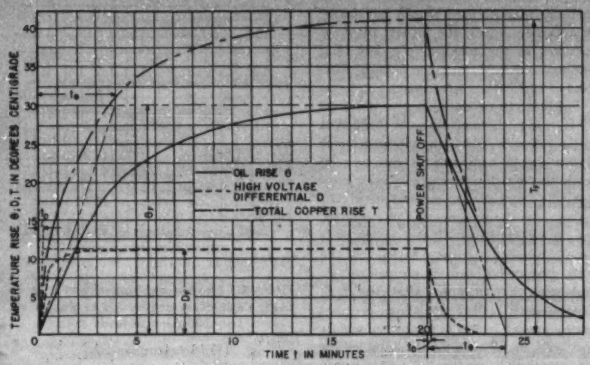


Figure 1. Temperature rises in a small, single-phase distribution transformer at 100 per cent load

t = time since the start of the test; minutes
t_θ, t_D = time constants of the oil rise and of the differential; minutes

If the transformer is at a uniform ambient temperature in the beginning of the heating period, no heat energy is supplied to the oil itself at that moment. For this reason, the initial rate of increase is zero in the oil rise, θ, in Figure 2. A finite θ appears only after the windings have started to dissipate heat into the oil. This θ reaches a maximum rate of increase when the relationship between the heat energies dissipated from the windings and from the oil is such that heat is stored in the oil at a maximum rate. For similar reasons, the initial slope of the cooling curve of θ is not the same as its maximum rate of drop, which is reached some time after shutdown.

In the normal-load test (Figure 1) the heating and cooling times up to those maximum slopes become so small, as compared with the total running time, that the initial slopes of the θ curve are identified by the corresponding maximum values. Even then the initial rates of increase or decrease are much more rapid for D than for θ, but although D lies above θ at the start of the heating cycle, it becomes much less at the end, that is, $D_F << \theta_F$. These conditions are indicated graphically on Figure 1 by the time constants t_θ and t_D of the θ and D curves, which are the times required for the heating curves to reach final values θ_F and D_F without heat dissipation, or which are the times permitting the cooling curves to return to zero at the shutdown rate of heat dissipation. In the illustration, Figure 1, t_θ is nearly 20 times t_D. Also, since θ_F is much greater than D_F, the value of θ_F becomes by far the larger component of T_F. If, therefore, θ_F is measured accurately, and if D_F is read less accurately, the error in T_F resulting from the error in D_F will be small.

At higher overloads, the foregoing conditions are reversed, because the power has to be shut off in the lower part of the heating cycle, that is, long before θ has reached its steady value, and when D is still much higher than θ. For example, in Figure 2, the low-voltage differential D at shutdown is 118 while the oil rise θ is only 24. Consequently, D should now be determined with a higher degree of accuracy than θ. In this connection, note the very rapid decrease in D after shutdown. Also, the rate of decrease differs greatly for the two windings, being 35 degrees centigrade per minute in the low-voltage winding, but only nine in the high-voltage winding, at the shutdown point. Thus any measurement of D, made after shutdown, is subject to a large correction which may be quite different for the two windings. In the case shown, the total copper rise T of the low-voltage winding decreases to less than 50 per cent of its value at shutdown in three minutes.

Furthermore, certain variations of the differential prior to shutdown are not measurable by ordinary test methods. For example, Figure 3 shows the result of an overload test in which the differential reaches a maximum before shutdown, and then decreases, although the copper and oil temperatures continue to rise. Such an effect, produced by increased cooling efficiency at higher temperatures, is of importance in making calculations based on test results.

Limitations of Conventional Test Procedures

According to AIEE and American Standard Association rules, three methods are available for the test of these winding temperatures.[7] In each method, the rise is determined by one or more measurements after shutdown, and the only difference between the three procedures is the nature of the correction applied to determine the rise at shutdown.

One method requires that a correction of one degree centigrade be added to the measured value, for each minute elapsed between the measurement and shutdown, and applies only up to seven watts per pound winding losses, because the initial slope of a cooling curve is equal to the dissipated losses divided by the thermal capacity of the coils,[8] and a slope of unity implies that the losses are approximately equal to the capacity, that is, to a value which lies usually between three and eight watt-minutes per degree centigrade per pound.

A second method, introduced for higher losses up to 30 watts per pound, uses as correction the product of these losses with a factor which depends on the time between test and shutdown.

A third method, recommended for losses above 30 watts per pound, requires

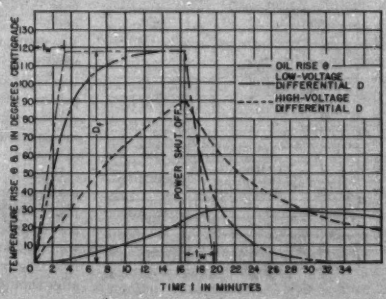


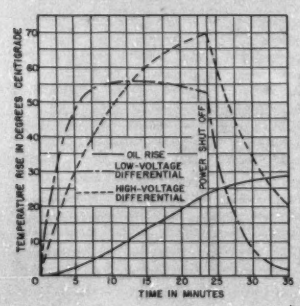
Figure 2. Temperature rises in a small, single-phase distribution transformer at 400 per cent load

the measurement of the lower part of a cooling curve by a series of readings whose extrapolation furnishes the temperature at shutdown. Where very rapid temperature changes are involved, such as those of the low-voltage differential shown in Figure 2, the timing and extrapolation of these readings may be subject to considerable error.

These methods furnish the copper temperature at the instant of shutdown only, and on this basis, the corresponding heating curve can be calculated with but a limited degree of accuracy, as shown in Figure 4, by comparison with a tested curve. Overload tests, however, are usually made to obtain basic data for the calculation of the temperatures existing under any load conditions, or of the temperature variations produced by a cycle of widely changing load. In either case, the variation of copper and oil temperatures throughout the loading cycle should be measured. Attempts have been made to do this by the above methods, that is by making repeated shutdowns for periods long enough to obtain a resistance measurement, or by repeating the test a number of times and by shutting down after various time periods.[9] These procedures, however, are laborious, and are subject to the limitations set forth before.

Various systems have been proposed for calculating the temperature variations at heavy overloads from the results obtained at several moderate continuous overloads. These methods are not very accurate, since small errors, made in the measurements, are multiplied manyfold in an extrapolation to heavy loads. Also, since the variation of temperature rises and time constants with load is quite complex,[5,9] the extrapolation is difficult unless simplifying assumptions are made which do not give reliable results. The methods may be used with a fair degree of accuracy only if the highest overload involved in the calculation is less than one and one-half times the highest overload at which tests were made.

Figure 3. Temperature rises in a small, 3-phase distribution transformer at 375 per cent load



Fairly accurate data may be obtained by the use of thermocouples, embedded at various locations in the windings, since thermocouple temperatures can be read while the transformer carries its a-c load; but this practice has several disadvantages. By the presence of these couples, the insulation level of the windings is usually reduced to such an extent that the coils cannot be used in normal service and must be scrapped after the test. Consequently, the procedure becomes uneconomical, except in the case of simple test coils or of transformer samples for large quantity production. The lowering of the insulation level may also prevent testing under normal excitation. For finding an accurate picture of a transient temperature distribution, a large number of readings must be taken in a short time. Since the presence of the couples alters the heat flow conditions in the windings, it may be impossible to make the number of couples large enough for obtaining a good average value of the temperature. Hence, the resistance test is preferable to the use of thermocouples when measuring average winding temperatures.

New Test Procedure

In order to include the advantages, experienced with the use of thermocouples in the resistance measurements, a new technique has been developed which, as shown in this paper, permits continuous factory tests of average copper temperatures throughout any load cycle. This technique does not require the use of specially made units or of unusual test equipment. The system may be applied to single and polyphase transformers, with or without excitation, and will, in many cases, measure the temperature rises in two or more windings simultaneously. The accuracy and application of the method are limited neither by the magnitude of the winding losses nor by the value of the coil resistance, and repeated tests show that entire heating and cooling curves are reproduced with a maximum discrepancy of two per cent at any point.

Since in relatively rare cases, as, for example, in the test of individual single-phase units without taps, it will not be readily possible to connect the transformer windings for a temperature test under load, the readings will have to be taken after shutdown in the usual manner.

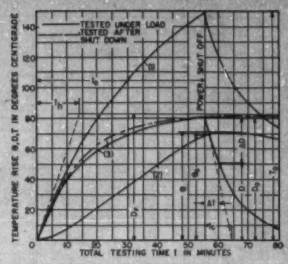


Figure 4. Heating and cooling curves in a small, single-phase distribution transformer at 325 per cent load

(1) Total copper rise T
(2) Oil rise θ
(3) Differential D

First, therefore, an attempt will be described to improve the evaluation of temperature tests made after shutdown. These methods permit more accurate determination of the temperatures at shutdown and of the heating transients. The computations may be based on a cooling curve, or, if the apparent thermal capacity of copper is known, on a single test point. A reasonable accuracy is obtained up to 75 watts per pound copper losses. This value corresponds to an initial cooling rate of 10 to 20 degrees centigrade per minute, and may be exceeded at 300 to 350 per cent load.

The Evaluation of Temperature Readings Obtained After Shutdown

In the evaluation of resistance tests of average winding temperatures present after shutdown, distinguish between the two conditions: that either a cooling curve, that is a series of test points is taken; or that readings are obtained only once at the earliest possible moment. In either case, the winding temperature passes through a heating and cooling transient before reaching the test point, as shown in Figure 4 for the short-time overload of a small transformer. Consequently, the evaluation must be made in two steps. First the test results are extrapolated along the cooling curve back to the moment of shutdown. Then the differential at shutdown is extrapolated along the heating curve to a final value. This method will be explained for the example of short-time overload tests of oil-insulated, or of other liquid-filled transformers, where the procedure is most complex, so that it can be applied to any

other type of insulation, cooling medium, and load conditions. Consequently, introduce

T_s = starting temperature; degrees centigrade

$T_a = 234.5 + T_s$ = starting temperature above the level of zero copper resistance; degrees centigrade

T = average copper rise above T_s at any time; degrees centigrade

θ = oil rise above T_s at any time; degrees centigrade

T_0, θ_0, D_0, t_0 = values of T, θ, D, t at shutdown

D_F, T_F = fictitious final or steady values of D and T; degrees centigrade

ΔD = correction of D between shutdown and test; degrees centigrade

Δt = time after shutdown; minutes

t_h = heating-time constant of the copper, that is, the time in which D would reach D_F without heat dissipation; minutes

t_c = cooling-time constant of the copper, that is, the time in which D would return to zero at the shutdown rate of heat dissipation; minutes

W_s, W_F = initial and final specific copper losses; watts per pound

r_0 = initial decrease of D after shutdown; degrees centigrade per minute

k = apparent thermal capacity of copper; watt-minutes per degree centigrade per pound

No appreciable exchange of heat energy between the oil and the outside air will take place during short-time overloads so that a variation in ambient with time can be neglected. For this reason, the rises T and θ are referred to the starting temperature T_s rather than to the ambient, as above, assuming that all transformer parts will be at the same temperature at the beginning of the test. Furthermore, the calculations extend, according to Figure 4, only over relatively small sections of the heating and cooling curves so that the rate of heat dissipation from each winding can be considered to change proportionally to its differential D. Then its cooling curve is represented by[10]

$$D = D_0 e^{-t/t_c} \tag{1}$$

In the analysis of the corresponding heating curve, the change of copper losses with temperature must be considered too. Actually, the oil rise θ and the differential D both contribute to any increase in temperature so that the losses as well as D and T can assume their final values only when θ becomes steady. However, in order to describe the heating transient of D by a simple mathematical function, fictitious quantities are introduced for the final values D_F and T_F on the assumption that the oil rise θ would retain its shutdown value θ_0 if the run were to be continued beyond the time t_0, that is,

$$T_F = D_F + \theta_0 \tag{2}$$

In addition to this, let D pass through the actual shutdown point D_0, t_0, and reach D_F under the assumption that the copper losses depend only on D and not on θ. Then D is given, as shown in the Appendix, by the function

$$D/D_F = 1 - e^{-t/t_h} \tag{3}$$

plotted in Figure 5. This curve has the same initial slope and shutdown point as the actual heating transient. The intermediate calculated values lie in the example, Figure 4, up to seven per cent above the test results, but are conservative, and can, therefore, be used for finding the copper temperature for shorter shutdown times than allowed in the experiment.

In these calculations, an apparent value must be employed for a thermal capacity k, referred to the weight of the active copper, by adding the heat stored in the winding insulation to that in the bare conductor. Like the temperature distribution, this apparent amount of k varies during any transient heating cycle. Note further that the time constant of the copper, referred to as t_D in Figures 1 and 2, actually has different values t_h and t_c in the heating and cooling periods, due to an increase in the specific losses from W_s to W_F, and in the capacity k between the start and the end of the heating curve. The slight change of k will be neglected.

In the test, the readings are usually given in terms of T and θ, and

$$D = T - \theta \tag{4}$$

Assume also that the oil rise θ is measured by thermocouple for the complete heat run.

Evaluation of Cooling Curves

Under these conditions, evaluate first a measured cooling curve. Its test points can be extrapolated to the moment of shutdown by using a French curve. The shutdown value of D_0 thus obtained depends on the shape of the ruler as well as on the judgment of its operator. In order to reduce these influences, the test points D of the cooling curve will be dropped to D_x on its tangent line at the shutdown point, as shown in Figure 6, so that the extrapolation can be made with a straight edge.

For this purpose, combine the corrections to be made between D and D_0, and between D and D_x

$$\Delta D = D_0 - D \qquad \Delta D_x = D - D_x \tag{5}$$

with equation 1 of the cooling curve, and with the function of its tangent line

$$D_x = D_0(1 - \Delta t/t_c) \tag{6}$$

and find

$$\frac{\Delta D_x}{\Delta D} = \frac{e^{-\Delta t/t_c} - (1 - \Delta t/t_c)}{1 - e^{-\Delta t/t_c}} \tag{7}$$

which is plotted against $\Delta t/t_c$ in Figure 7.

In a numerical calculation, extrapolate the test points back to shutdown with a French curve, lay a tangent line at the shutdown point, and obtain a first approximation of D_0 and t_c. For the corresponding values $\Delta t/t_c$, read $\Delta D_x/\Delta D$ from Figure 7, and compute ΔD, ΔD_x, and D_x by equation 5. A straight line through these points D_x will furnish a second approximation for D_0 and t_c which may be used to repeat the procedure. The method converges quickly, and the second approximation usually gives satisfactory results. A numerical example is given in

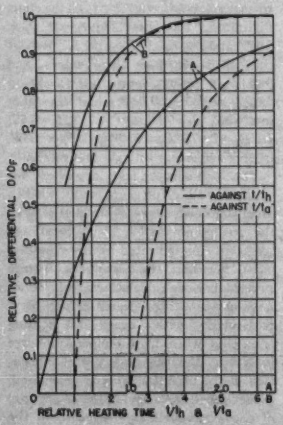
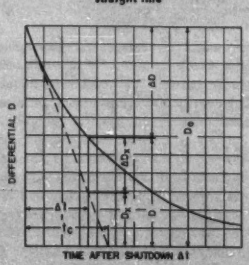


Figure 5 (left). Functions for calculation of heating transients

Figure 6. Extrapolation of a cooling curve to the moment of shutdown by conversion into a straight line



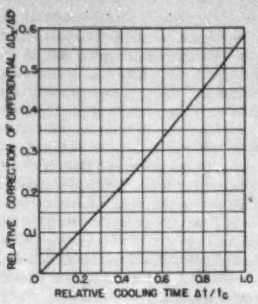


Figure 7. Correction for converting an experimental cooling curve into a straight line

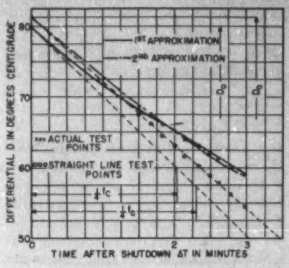


Figure 8. Extrapolation of a cooling curve of a small, single-phase distribution transformer at 325 per cent load

Figure 8 by an evaluation of the cooling curve, shown in Figure 3.

In the second step of evaluating a cooling curve, that is in the extrapolation of D_0 to D_F, combine

$$W_F = W_s\left(1+\frac{T_F}{T_a}\right) \tag{8}$$

with

$$t_h = \frac{k}{W_s} D_F \tag{9}$$

and

$$t_c = \frac{k}{W_F} D_F \tag{10}$$

to find

$$t_h = t_c\left(1+\frac{T_F}{T_a}\right) \tag{11}$$

For obtaining a first approximation of t_h, estimate the value of T_F, and use the amounts of t_c and D_0 found by extrapolation of the cooling curve. Introduce $t/t_h = t_0/t_h$ into Figure 5, and obtain $D_0/D_F = D/D_F$ and D_F. For the new value of T_F, computed by equation 2, repeat the calculation to find a second approximation for D_F. How quickly this procedure converges, will be shown by an extrapolation of the heating curve of D, Figure 3. For this purpose, note a starting temperature $T_s = 25.5$, or $T_a = 260$, a shut-off point $t_0 = 55.5$, $D_0 = 81.2$, $\theta_0 = 68.4$, $T_0 = 149.6$, and also $t_c = 9.1$, obtained from direct tests and from Figure 8. Assume, for example, $T_F = 160$, and compute $t_h = 14.7$, $t_0/t_h = 3.78$, $D_0/D_F = 0.980$, $D_F = 82.9$, and $T_F = 151.3$ by equation 10 and Figure 5. With this new quantity T_F, find a second approximation of $t_h = 14.2$, $t_0/t_h = 3.91$, $D_0/D_F = 0.983$, $D_F = 82.7$ and $T_F = 151.1$. These quantities furnish the capacity k at shutdown by equations 8 and 10, if the specific losses $W_s = 27.1$ are obtained from the coil design. Then compute $W_F = 41.2$, and $k = 4.53$.

Evaluation of a Single Test Point Obtained After Shutdown

When only one test point is taken after shutdown, extrapolate D, equation 4, to D_0 first for the condition of a Δt which is small as compared with t_c; that is, approximate the cooling curve of D by its tangent line at the shutdown point. For example, with a $\Delta t \leq 0.20\ t_c$, the discrepancy between the approximation and equation 1 will be less than ten per cent of the correction to be made. Under this condition, the shutdown slope of the cooling curve is represented by

$$r_0 = \frac{W_F}{k}\frac{D_0}{D_F} \tag{12}$$

where W_F is given by equation 7. For the evaluation, use an estimate for T_F and D_0/D_F, and compute k from the coil dimensions on the basis that the thermal capacity of active copper has a value of about three watt-minutes per degree centigrade per pound, and that the thermal capacity per unit volume of the winding insulation is approximately half as high as for copper.[11] Then k is found by introducing a winding space factor

$$S = \lambda\frac{\text{Active copper section}}{\text{Insulated coil section}} \tag{13}$$

The quantity λ represents an experience factor which has been found to lie, for example, between 0.80 and 1.00 for liquid-filled distribution transformers. Consequently, use

$$k = 3\frac{1+S}{2S} \tag{14}$$

in equation 12, compute

$$\Delta D = r\,\Delta t \tag{15}$$

and find D_0 by equation 5.

If Δt is not small as compared with t_c, that is, for example, with $\Delta t > 0.20\ t_c$, calculate t_c of equation 10 by finding D_F, W_F, and k in the same way as before, and obtain D_0 by equation 1.

From this point, the values of D_F and T_F can be extrapolated as in the evaluation of a cooling curve, but since the calculated value of k is independent of an estimate for D_F, obtain a shorter and explicit procedure by developing equations 3 and 9 into

$$D/D_F = 1 - e^{-t\frac{W_s}{kD}\frac{D}{D_F}} \tag{16}$$

Introduce an apparent time constant

$$t_a = \frac{k}{W_s} D \tag{17}$$

and find the function

$$t/t_a = -\frac{D_F}{D}\log\left(1-\frac{D}{D_F}\right) \tag{18}$$

which has been added to the chart, Figure 5, and which furnishes $D_F/D_0 = D_F/D$ for any given values of $t/t_a = t_0/t_a$.

For an example, use a single test point $T = 138.0$, $\theta = 69.5$, and $\Delta t = 1.47$ on the cooling curves of Figure 3, in addition to $T_s = 25.5$, $T_a = 260$, $t_0 = 55.5$, and $\theta_0 = 68.4$. Assume also that $W_s = 27.1$ and an approximate value of $k = 5.0$ are derived from the rating and dimensions of the winding. Estimate $D_0/D_F = 0.95$, $D_1 = 75.0$, that is, $T_F = 143.4$, and find, by equations 8, 12, 15, and 5, the values $W_F = 42.0$, $r_0 = 8.0$, $\Delta D = 11.8$ and $D_0 = 80.3$; or compute, by equations 10 and 1, the quantities $t_c = 8.9$, $D_0 = 80.8$, and $T_0 = 149.2$. Use the last amount of D_0 for D in equation 17, and obtain $t_a = 14.9$, whence $t/t_a = t_0/t_a = 3.72$. Then the chart Figure 5, furnishes $D_0/D_F = D/D_F = 0.972$, and $D_F = 83.1$, $T_F = 151.5$. Repeat the calculation with these values, and find, as a second approximation, $W_F = 42.9$, $r_0 = 8.35$, $\Delta D = 12.1$ and $D_0 = 80.6$; or $t_c = 9.7$, $D_0 = 79.8$, $t_a = 14.7$, $t_0/t_a = 3.77$, $D_0/D_F = 0.975$, $D_F = 81.8$, and $T_F = 150.2$, in close agreement with the first results.

The procedures just described for evaluation of temperature readings taken after shutdown improve the accuracy of the results obtainable by conventional methods. As indicated by the examples cited, however, these methods require rather laborious calculations, and still do not achieve a high degree of accuracy, particularly with regard to heating transients when high temperatures are involved. For this reason, the application of the procedure should be limited, as mentioned at the end of the description of conventional test procedures, to the

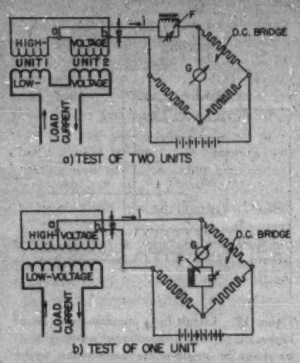


Figure 9. Circuits for a test of the high-voltage winding temperature in single-phase transformers under load, without line voltage

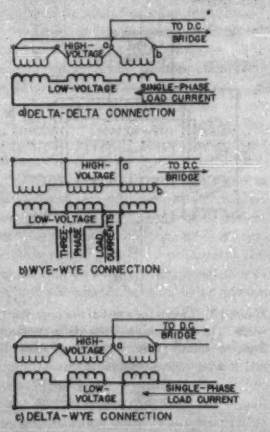


Figure 10. Circuits for a test of the high-voltage winding temperatures in three-phase transformers under load, without line voltage

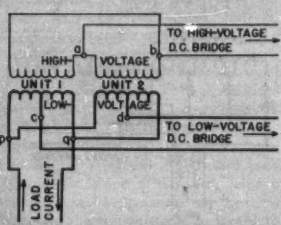


Figure 11. Circuit for a simultaneous test of high-voltage and low-voltage winding temperatures in single-phase transformers under load, without line voltage

few cases to which the new technique of testing under load does not apply, because the test results become much more accurate, are much easier to evaluate, and may cover much more territory when using that new technique as explained by the following.

Temperature Tests Under Load

Most of the inherent errors and tedious computations, associated with indirect factory measurements of average coil temperatures, are eliminated, if these temperatures are tested while the transformer carries load, rather than after shutdown.

In this connection, it has already been pointed out that it is preferable to find these average temperatures from the corresponding change of the winding resistance. In order to make the method applicable, even when extremely high or extremely low coil resistances are involved, the principle of the Wheatstone or Kelvin bridge, employed for the conventional test of copper temperatures, is also used for making measurements under load; that is, direct current is supplied to the windings while they are under a-c excitation. Consequently, the d-c instruments and their operating personnel should be protected against the a-c voltage, and any alternating current entering these instruments must be reduced to an insignificant amount. Vice versa, the direct current sent into the windings should not enter the a-c supply circuits, since the resistance of these circuits would be included in the test results and would cause an error in the temperature readings. In order to comply with these requirements, the winding resistance is measured between points of substantially equal a-c potential, while the a-c excitation is supplied to terminals of practically equal d-c potential.

This a-c excitation of a transformer is known to consist of two components. One component is associated with the line voltage and core losses; the other component is identified with the load current and winding losses which cause the temperature rises to be measured. Note that the core losses are essentially independent of the load. Consequently, a core loss, for example, of 25 per cent of the total losses at rated load will amount to only about 2 per cent, at 400 per cent load, and will, therefore, have a negligible effect upon the temperatures. For this reason, the line voltage can be omitted in measurements at high overloads, and the load current is applied in a similar manner as in conventional short-circuit tests of winding losses.[12] Then, the a-c potential, to which the d-c circuit may be exposed, is reduced to a small fraction of the line voltage, and it becomes easier to protect the d-c instruments and their operators. At the same time, the connections for making measurements under load are considerably simplified.

Tests Without Applying Line Voltage

Figure 9 represents some typical examples of circuits without line voltage, and solves the problem of a temperature test under load for the high-voltage windings of single-phase transformers. In Figure 9(a), the low-voltage windings of two identical units are excited in series while their high-voltage windings are short-circuited in series. Then the high-voltage terminals a and b are at the same a-c potential, and can be connected to a d-c bridge which measures the two high-voltage winding resistances in parallel. An ordinary Wheatstone or Kelvin Bridge may be used for this purpose. The direct currents flow continuously during the whole temperature run, and must, therefore, be kept small enough not to contribute materially to the winding losses.

The same test can be made on a single unit with a connection shown in Figure 9(b). As found by experiment, the a-c voltage between any tap a and the common terminal b of a short-circuited winding is sufficiently small so that a d-c bridge can be connected to those two points. Then, the resistances of the two winding sections are tested in parallel. Tap a, therefore, should be located so that these two resistances become substantially alike, and the true average of the winding temperatures is obtained from the bridge readings. In the absence of any tap, an individual single-phase unit cannot readily be measured under load, but may be tested after shutdown, as described in the foregoing.

Because of manufacturing variations, and of lead and contact resistances, the alternating voltages and currents e and i at the winding terminals a and b are, even in the case of Figure 9(a), never zero, but are found to be of the same order as in Figure 9(b). This residual a-c excitation, supplied to the d-c bridge, can be characterized by the open-circuit voltage e appearing between a and b when the bridge is disconnected, and by the short-circuit current i flowing in a direct link between those terminals. In practice, values of open-circuit voltages e from 100 millivolts to 60 volts and of short-circuit currents i from 100 microamperes to 1 ampere, have been measured, depending

upon the rating of the winding under test. The actual amounts of e and i supplied to the bridge will be considerably smaller since the bridge resistances represent an a-c load at the terminals a and b of the transformer. For further reduction of this residual a-c excitation, a filter F, consisting of a variable capacitor and of an iron core reactor, can be inserted, as shown in Figures 9(a) and 9(b). If connected as in Figure 9(a), the reactor coil must be dimensioned to carry, during the whole temperature run, a direct current of perhaps several amperes which is supplied to the windings. In addition to this, the reactor resistance enters the test results like any invariable lead or contact resistance, and must, therefore, be kept small as compared with the winding resistance. A reactor complying with these requirements is about as big as the transformer to be tested.

The size of such a current-limiting device is considerably reduced by the connection in Figure 9(b), where it becomes practically independent of the unit to be tested. Note that a-c excitation does not disturb the d-c balance of a bridge, so that a residual alternating current may enter the bridge resistances, as long as they do not become overheated. Then, only the galvanometer G must be protected, and the reactor will be designed to carry at most a few milliamperes flowing through that galvanometer. Furthermore, this reactor is not in the winding circuit, and does not introduce an error into the measurements.

In many cases, satisfactory results will be obtained with such a reactor alone, or even without any current limiting device.

A protection against an excessive a-c voltage appearing at the bridge, due to wrong connections or winding failures, may be obtained by placing cutouts between terminals a and b, and between one terminal and ground. If the displacement current between the winding under test and ground can become appreciable, it may be advisable to replace the second cutout by a solid ground.

An application of the methods, introduced in Figure 9, to temperature tests of three-phase high-voltage windings under load, without line voltage, is presented by Figure 10. In the delta-delta connection, Figure 10(a), single-phase load current is circulated through the three low-voltage phases, while two high-voltage phases are connected and tested in the same way as in Figure 9(a). The third high-voltage phase is short-circuited separately. In the wye-wye connection, Figure 10(b), three-phase load currents are supplied to the low-voltage windings. The neutral a and the common short-circuit point b of the three high-voltage phases will be at the same a-c potential, and can, therefore, be connected to a d-c bridge which will measure the three winding resistances in parallel. In the delta-wye connection, Figure 10(c), the three low-voltage phases are excited in parallel by a single-phase load current, while the high-voltage side is connected and tested in the same way as in Figure 10(a).

If the temperatures of the high-voltage and low-voltage windings of a single-phase transformer are to be read in the same run, two units may be connected as shown in Figure 11. In this circuit, load current is supplied to the terminals p and q of a parallel connection of the low-voltage sides, while the high-voltage windings are short-circuited as in Figure 9(a). Then, not only the high-voltage terminals a and b, but also two low-voltage midtaps c and d will be at the same a-c potential, and are connected to separate bridges which are distinguished in Figure 11 as high-voltage and low-voltage bridges. The direct current in the low-voltage windings is divided between two parallel paths with d-c resistances of which the terminals p and q are the center points. Consequently, p and q are at the same d-c potential, as required.

Tests When Applying Line Voltage

These circuits must be modified considerably when the core losses cannot be neglected in comparison with the load losses, so that line voltage must be applied to the windings, as, for example, in the test at rated load of high-temperature dry-type transformers. Figure 12 shows a circuit for a simultaneous measurement of both sides of two single-phase units. The a-c line voltage is supplied to common terminals m and n of a parallel connection of the two high-voltage windings. Each low-voltage winding is divided into two sections with equal numbers of turns. The individual sections of the first unit are connected in series with the corresponding sections of the second unit. The two circuits, thus obtained, are connected in parallel, and load current is supplied through terminals p and q. Then, mid points c and d of these two low-voltage circuits, and midtaps a and b of the two high-voltage windings will be at equal a-c potentials for the d-c test. Note also that the same d-c potentials appear at the a-c supply terminals m and n, as well as at p and q, as required.

In another example, shown in Figure 13, the method of testing temperatures under load with line voltage is applied to the

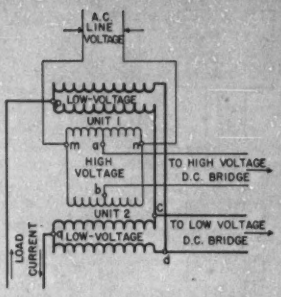


Figure 12. Circuit for a simultaneous test of high-voltage and low-voltage winding temperatures in single-phase transformers under load and line voltage

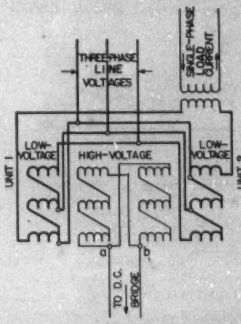


Figure 13. Circuit for a test of the high-voltage winding temperatures in delta-delta connected 3-phase transformers under load and line voltage

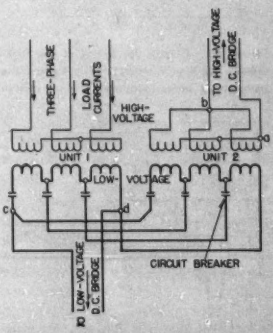


Figure 14. Circuit for a simultaneous test of high-voltage and low-voltage winding temperatures in Y-delta connected 3-phase transformers under load without line voltage

high-voltage windings of two delta-delta connected three-phase units. Their low-voltage sides are excited in parallel by three-phase line voltage while single-phase load current is circulated in series through the windings. All high-voltage windings are connected in series in a closed circuit, so that the common terminals *a* and *b* of the two units are at equal a-c potential for the d-c test.

During these measurements under line voltage, the d-c bridges and their personnel may be protected against abnormal alternating currents and voltages in the same way as discussed in connection with Figure 9 for circuits without line voltage.

Test of Transformers Containing Circuit Breakers

Transformers containing circuit breakers present a special problem in temperature measurements at short-time overloads, because such loads are interrupted by these circuit breakers in order to prevent overheating of the windings. For insuring proper circuit breaker operation, the same current must flow through each circuit breaker in the test as under actual service conditions. If, for example, the breakers are normally placed in the low-voltage line leads of a delta-delta connected 3-phase transformer, single-phase load current should not be circulated through the windings, since such a current would by-pass the circuit breakers. Furthermore, in a test of several units, the load current has to be interrupted everywhere if the circuit breaker trips in one unit only.

A typical example of a circuit complying with these requirements is shown in Figure 14 for the simultaneous test of the high-voltage and low-voltage windings of two Y-delta connected 3-phase transformers without line voltage. These units are protected by circuit breakers placed in the low-voltage line. In order to let either circuit breaker interrupt the load currents of both units, they are loaded in cascade formation; that is, three-phase currents are supplied to the high-voltage side of unit *1* while the high-voltage terminals of unit *2* are short-circuited. Then the high-voltage windings of unit *2* can be tested in the same way as in Figure 10(b). On the low-voltage side, the corresponding line terminals of the two units are interconnected, and each delta circuit is opened to obtain points *c* and *d* of equal a-c potential, for the same kind of a d-c test as made on the high-voltage windings, Figure 13. Note also that two circuit breaker poles of each unit carry line currents, while only phase current passes through the third pole. This condition will insure proper breaker operation if the three circuit breaker contacts are mechanically interconnected, so that they will trip at the same time.

For a corresponding simultaneous test of two single-phase transformers with low-voltage circuit breakers, the low-voltage windings may be connected as shown in Figure 12, in order to insure interruption of all load current by the tripping of either.

Results of Temperature Tests Under Load

Examples of heating transients, obtained under load by the above methods, are found in Figures 1 to 4. In particular, Figures 1 to 3 contain temperature rises measured in some single-phase transformers, whereas Figure 4 shows the corresponding heating and cooling curves appearing at a short-time overload of a 3-phase unit.

Conclusions

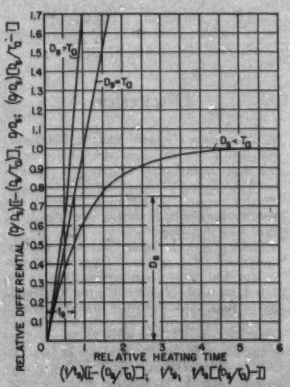
The methods developed in this paper are useful tools with which a better picture of the thermal characteristics of transformers can be obtained. The accuracy represents a great improvement over the results furnished by conventional measurements since test values become reproducible with a maximum discrepancy of two per cent even when taking readings during rapid temperature changes. The constants and thermal capacities of windings may now be studied under load and under various heating and cooling conditions. Additional information may be obtained concerning the effect of short-time overloads upon the life expectancy of transformers. Furthermore, it becomes possible to check existing methods for calculating temperature transients with reliable test values so that these methods can be improved if necessary. The practice of obtaining resistance readings under load greatly reduces the effort required for an evaluation, and, consequently, is well suited for commercial applications.

Figure 15. Effect of the variation of losses with temperature upon the heating transients of the winding differential

$D_s < T_a$:
$(D/D_s)(1-D_s/T_a)$ against $(t/t_s)(1-D_s/T_a)$

$D_s = T_a$:
D/D_s against t/t_s

$D_s > T_a$:
$(D/D_s)(D_s/T_a-1)$ against $(t/t_s)(D_s/T_a-1)$



Appendix. Influence of a Loss Increase with Temperature upon the Thermal Transients of a Winding

Neglect any increase in losses with the oil rise θ, and introduce

L = winding losses at any moment t; watts
L_s, L_F = values of L at the start and at the end of the heating transient; watts
α = temperature coefficient of the winding resistance; 1/(degrees centigrade)
K = thermal capacitance of the winding; watt-minutes per degree centigrade

According to the principle of energy conservation, the heat balance in the winding will be, at any instant,

$$\text{Stored Heat} + \text{Dissipated Heat} = \text{Generated Losses} \quad (19)$$

Let the dissipated heat be proportional to the differential D, and note that all losses are dissipated from the winding at the steady end of the transient. Then, obtain in any small time interval δt, and for a corresponding small increment δD of D

$$\text{Stored Heat} = K\delta D$$

$$\text{Dissipated Heat} = L_F \frac{D}{D_F}\,\delta t$$

$$\text{Generated Losses} = L\delta t$$

and find

$$K\delta D + L_F \frac{D}{D_F}\delta t = L\delta t \quad (20)$$

By substituting

$$L = L_s(1+\alpha D) \quad (21)$$
$$L_F = L_s(1+\alpha D_F)$$

relation 20 can be transformed into the differential equation

$$K\frac{dD}{dt} + L_s\frac{D}{D_F} = L_s \quad (22)$$

in which the temperature coefficient α no longer appears. Note also that

$$\frac{L_s}{K} = \frac{W_s}{h} \quad (23)$$

and introduce the heating-time constant given in equation 9

$$t_h = \frac{k}{W_s} D_F$$

that is, the time in which D would reach D_F without heat dissipation. Then, equation 22 may be written

$$\frac{dD}{dt} + \frac{D}{t_h} = \frac{D_F}{t_h} \tag{24}$$

and has the solution previously given in equation 3

$$D = D_F(1 - \varepsilon^{-t/t_h})$$

Although this transient is derived for variable winding losses, it has the same form as for constant losses.[10] The only differences lie in the numerical values to be used for D_F and t_h. The quantity D_F depends solely on the losses at the end of the heating period while the value of t_h is determined also by the losses at the start, as shown by equation 9.

The general character of equation 3 can also be illustrated by introducing the quantities

D_s = steady value of D for $\alpha = 0$, that is, the value reached under the condition of winding losses L_s at the end of the heating transient; degrees centigrade

t_s = heating time constant for $\alpha = 0$, that is, the time required in which the heating transient would reach D_s without heat dissipation; minutes

Because of the proportionality between D and the dissipated heat, find, instead of equation 21,

$$\left.\begin{aligned} D_F &= D_s(1 + \alpha D_F) \\ D_F &= D_s/(1 - \alpha D_s) \end{aligned}\right\} \tag{25}$$

Write further

$$\alpha = 1/T_a \tag{26}$$

and

$$t_s = \frac{k}{W_s} D_s = t_h/(1 - D_s/T_a)$$

and introduce the equations 9 and 26 into equation 3 to derive

$$\left.\begin{aligned} &D = \frac{D_s}{1 - D_s/T_a}\left(1 - \varepsilon^{-\frac{t}{t_s}(1 - D_s/T_a)}\right) \\ &\text{for } D_s < T_a \\ &D = D_s \frac{t}{t_s} \\ &\text{or } D_s = T_a \\ &D = \frac{D_s}{D_s/T_a - 1}\left(\varepsilon^{+\frac{t}{t_s}(D_s/T_a - 1)} - 1\right) \\ &\text{for } D_s > T_a. \end{aligned}\right\} \tag{27}$$

As shown by relative values in Figure 15, the heating transient of D retains for variable winding losses its conventional form, Figure 5, only under the condition that the final differential D_s remains smaller than the starting temperature T_a above the level of zero losses. For $D_s = T_a$, however, the heating curve goes over into a straight line while for $D_s > T_a$, this chart obtains an exponential form with an opposite curvature as for $D_s < T_a$. Neither one of the last two transients reaches a finite steady value D_F, that is, the winding temperature increases indefinitely when D_s attains or exceeds a critical amount T_a. Such a condition may be reached in the operation of high temperature dry-type transformers.

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Discussion

M. F. Beavers (General Electric Company, Pittsfield, Mass.): The desirability of a practical method of accurately and continuously measuring the winding resistances of a transformer for purposes of determining the winding temperature during heat run tests cannot be questioned.

To be practical, the method must employ equipment with reasonable proportions as to size and kilovolt-ampere ratings to be tested, must be safe for operating personnel, and be reasonable in cost of installation and upkeep. The authors indicate that the reactor employed in the filter in Figure 9(a) of the paper is physically about as large as the transformer being tested. This would seem to limit the application of this scheme to small transformers. The scheme shown in Figure 9(b) of the paper appears to be more suitable for large power transformers. It is not clear from the paper whether one of these schemes is more accurate or suitable than the other, but if they are comparable in all other respects it appears that the scheme in Figure 9(b) of the paper would be preferable from the standpoint of size of filter equipment alone.

Both of these schemes require that a midtap or connection be available in each winding to be measured when testing a single-phase unit. Such midwinding connections are seldom present in 2-winding transformers and are quite rare in 3-winding types. This requirement alone will usually make this method of resistance measurement unsuitable for tests on a single unit of a production design. However, if tests are to be made on two similar units, or on a development basis, midpoint taps may be provided for the purpose of the test.

If two or more windings are to be tested simultaneously, two or more d-c bridges will be required. Of course, one bridge may be used if suitable switching facilities are available and if it is not necessary to obtain simultaneous readings on all windings.

With regard to accuracy, the authors present but little evidence in the way of test data to substantiate the claim that their method is more accurate than present methods of making temperature tests. With this method, particular attention must be given to obtaining good connections to the winding terminals so as not to introduce errors due to poor contact resistance and to overheated connecting leads.

This method should be particularly useful in developmental work involving temperatures under transient heating and cooling conditions where present methods are not suitable for observing the temperature-time characteristics of windings under load conditions. As a substitute for the present methods of making commercial temperature tests (continuous rated load) it is believed that considerably more experience with the proposed method will be required before the method can be considered for making commercial acceptance tests.

The proposed method offers to the industry another tool in the search for better understanding of transformer thermal characteristics, and the authors are commended on their work as a step in this direction.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The authors have devoted considerable space to the derivation of a formula for calculating the cooling after shutdown. This formula has been given many times in the literature[1] and I used it for many years but abandoned it a few years ago. The reason I abandoned it is that it applies only when loss of heat is proportional to temperature rise under ultimate conditions. In practically all cases for oil-immersed windings, the loss is proportional to the temperature rise over oil, raised to an exponential power ranging from about 1.20 to approximately 1.43, depending on the conditions of cooling. Many tests have shown that under certain conditions the exponential power should be factored when calculating the cooling after shutdown.

The equation that applies was derived in an AIEE paper,[3] and is of the form

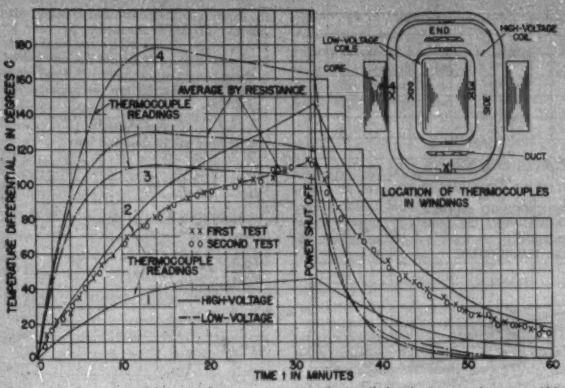

Figure 1. Temperature differentials in the windings of a small distribution transformer at 400 per cent load

$$\theta = \theta_0 \left[1 + (n-1)\frac{t}{B}\right]^{\frac{-1}{n-1}}$$

where

θ = temperature rise at any time "t"
θ_0 = initial temperature rise over oil at shutdown
n = exponent of ultimate temperature rise (θ^n) versus loss
B = time constant

The difference in results obtained by the two formulas is illustrated by the following example; assuming

$\theta_0 = 30$ degrees centigrade
$n = 1.25$
$B = 3$
$t = 4$ minutes

The cooling obtained by the formula given in the paper is 11 degrees, as against 12.3 degrees obtained by the foregoing formula.

The error is of course small for normal-load conditions but under heavy-overload conditions the error is too large to ignore.

In extrapolating cooling curves back to shutdown, I have found that the most convenient and most accurate method is to plot the increments of change in resistance versus time on semi-log paper. Resistance increments are plotted on the log scale and time on the linear scale. When the correct average oil temperature to which the winding is cooling is chosen, the test points all fall on a straight line. If the average oil temperature chosen is too high or too low, the test points will fall on a curve bending either downward or upward respectively. A little experience will soon enable one to easily extrapolate any cooling curve back to shutdown with a high degree of accuracy. This method is described and illustrated in Figure 27 of another article.[8]

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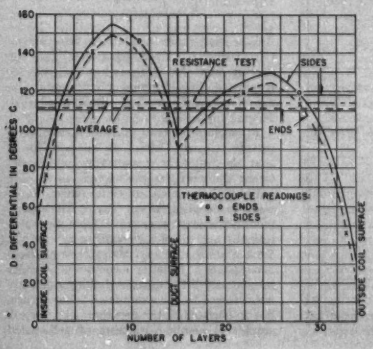


Figure 2. Temperature distribution tested at shutdown in the high-voltage coil of a small distribution transformer at 400 per cent load

A. M. Lockie and G. M. Stein: The discussions by Beavers and Montsinger indicate that certain conditions described in the paper should be elaborated upon more in detail.

Beavers questions whether a reactor should be used for the protection of the bridge, as shown in Figure 9(a) or as in Figure 9(b) of the paper. The answer is that the reactor should be installed as in Figure 9(b), since then it can be much smaller than in Figure 9(a), but serves the same purpose. Furthermore, in Figure 9(b) the reactor becomes independent of the size of the unit to be measured so that the same choke may be used for the test of a small distribution transformer as for a large power transformer. If such a reactor is properly designed, that is, with a low power factor, the presence of such a reactor is practically without influence upon the accuracy of the test.

It has been conceded in the paper that, as Beavers points out again, two identical units or a single unit with a tap are required for a temperature test under load, and that the method does not apply to individual units without tap.

It is also of concern to Beavers that more than one bridge has to be used for the simultaneous test of several windings. The corresponding winding resistances are very frequently so much different in size that the measurements cannot be taken with the same bridge even in a test made after shutdown. Consequently, the use of several bridges in a temperature test under load will, in general, not constitute an increase of equipment over previous requirements.

If Beavers wants further evidence of an improvement in accuracy, he may note that a direct reading of the winding temperature at the moment of shutdown will supply this value necessarily with much higher accuracy than an extrapolation of measurements obtained only some time after shutdown. In addition to this, the new technique furnishes the whole average heating curve of the winding. As shown in Figure 3 of the paper and Figure 1 of the discussion, the differentials of such heating curves may contain maxima and turning points, a result which has never been found by the present method of making resistance measurements after shutdown only. In a repeat test of the condition (Figure 1 of the discussion), the whole transient temperature curve has been reproduced with an accuracy of ±2 per cent.

Furthermore, these resistance measurements of the high-voltage and low-voltage windings are compared, in Figure 1 of the discussion, with readings of thermocouples distributed in those coils. It is of importance that the transient differentials obtained with these thermocouples have the same peculiar shape as found by the resistance tests. From the high-voltage thermocouple readings (Figure 1 of the discussion) the temperature distribution in the high-voltage winding sections at the ends and under the iron has been derived, as shown in Figure 2 of the discussion for the moment of shutdown. The average of these two distributions agrees quite well with the average obtained by the resistance test.

Beavers stresses also the necessity of good bridge connections to the winding terminals. This requirement is not new or unusual, since it had to be observed to the same ex-

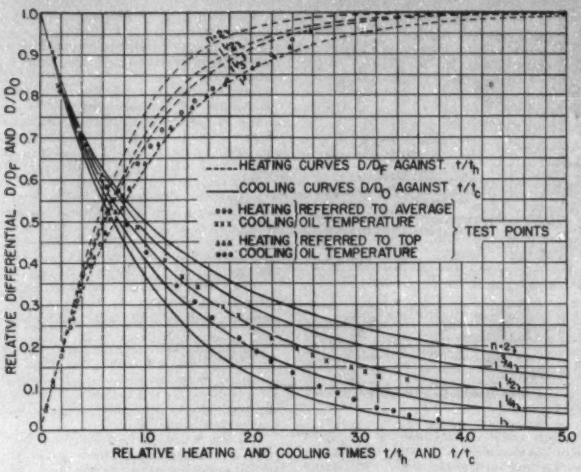


Figure 3. Analytic heating and cooling curves for different exponents of heat dissipation

tent in all previous resistance measurements of winding temperatures.

The new technique has been developed particularly for the test of the extraordinary high-winding temperatures occurring, for example, at heavy overloads of oil-filled units or in dry-type transformers with class *C* insulation. According to the introductory paragraphs of the paper, the technique is not intended to replace the present methods in commercial tests at continuous rated load of normal designs, as Beavers seems to believe, since for such tests the present methods can be considered as adequate.

In contradistinction to a statement made by Montsinger, formula 1 of the paper for representing a cooling curve has not been derived by the authors in their paper, but has been taken from the available literature. (See reference 10 of the paper). Montsinger objects to the use of this formula because it is based on a dissipated loss which is proportional to the temperature differential. He recommends, instead, a formula derived on the assumption that those dissipated losses vary in proportion to an nth power of the differential.

The authors themselves have used extensively Montsinger's formula for the cooling curve, and equivalent equations for the heating curve, in order to check the character of tested thermal transients. The equations for the heating curves are derived under the assumption that the resistance of the winding can be considered as being independent of the temperature, or that an average value L_a is used for the total winding losses in place of L_t and L_F, introduced in the appendix of the paper. If, in addition to this, the dissipated heat is represented by $L_a\left(\frac{D}{D_F}\right)^n \delta t$ instead $L_F\left(\frac{D}{D_F}\right)\delta t$, equation 22 of the paper will be modified into

$$k\frac{dD}{dt}+L_a\left(\frac{D}{D_F}\right)^n=L_a \tag{1}$$

and furnish, instead of equation 24 of the paper,

$$\frac{d(D/D_F)}{d(t/t_h)}+\left(\frac{D}{D_F}\right)^n=L_a \tag{2}$$

and

$$t/t_h=\int\frac{d(D/D_F)}{1-\left(\frac{D}{D_F}\right)^n} \tag{3}$$

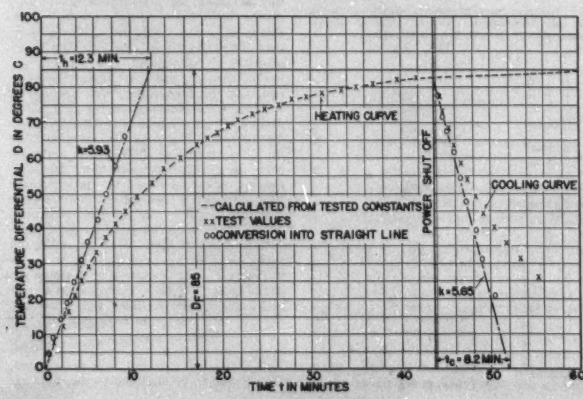
This expression may be integrated for any value of n which is a rational number; obtain, for example,

$$\left.\begin{aligned}
&\text{for } n=1\\
&t/t_h=-\log\left(1-\frac{D}{D_F}\right)\\
&n=4/3\\
&t/t_h=\frac{3}{2}\left[\tanh^{-1}\left(\frac{D}{D_F}\right)^{1/3}-\tan^{-1}\left(\frac{D}{D_F}\right)^{1/3}\right]\\
&n=3/2\\
&t/t_h=\frac{1}{3}\log\frac{1-\left(\frac{D}{D_F}\right)^{1/3}}{\left(1-\sqrt{\frac{D}{D_F}}\right)^2}-\frac{2}{\sqrt{3}}\tan^{-1}\frac{\sqrt{3\frac{D}{D_F}}}{2+\sqrt{\frac{D}{D_F}}}\\
&n=2\\
&t/t_h=\tanh^{-1}\left(\frac{D}{D_F}\right)
\end{aligned}\right\} \tag{4}$$

The heating curves of D/D_F as functions of t/t_h, found by these equations, and the cooling curves of D/D_0 as functions of t/t_c, computed by Montsinger's formula, are shown in the Figure 3 of the discussion for several values of n.

By adding the test points of a number of ratings to such a chart, it has been found that the exponent n may vary widely between different designs as well as in each individual temperature-time curve, especially in a cooling curve; that is, the experimental transient may travel from one theoretical transient of a constant n to another. Such a departure of the test results from Montsinger's formula becomes especially pronounced if the measured differentials are not referred to the average oil temperature, but to the top oil temperature, like, for example, in commercial tests of self-cooled

Figure 4. Evaluation of test results of transient high-voltage-winding temperatures in a small three-phase distribution transformer at 350 per cent load



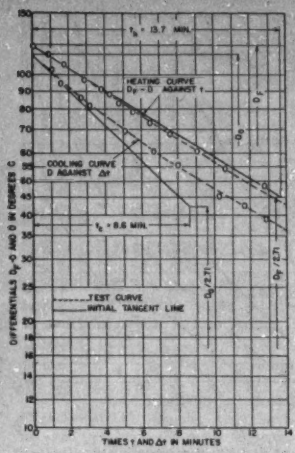
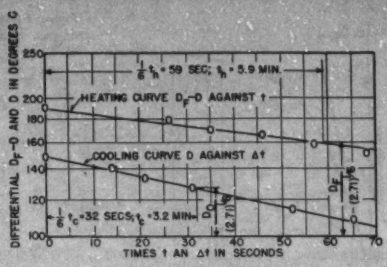


Figure 5 (left). Graphical evaluation of high-voltage heating and cooling curves measured in a small single-phase distribution transformer at 400 per cent load

Figure 6 (right). Graphical evaluation of initial high-voltage heating and cooling curves measured in a small distribution transformer at 600 per cent load



transformers, since the two oil temperatures do not change at the same rate with time. In the example, Figure 3 of the discussion, computed for the conditions of Figure 1 of the discussion, the tested differentials of the cooling curve lie in the neighborhood of $n = 1^1/_2$ when referred to the average oil temperature, but cross from $n = 1^1/_2$ to $n = 1$ when referred to the top oil temperature since these latter differentials have to become negative for sufficiently long times. The discrepancy between the corresponding analytical and tested heating curves in Figure 3 of the discussion, is not as big as that found for the cooling curves. In these heating transients, it also makes little difference whether the differentials are referred to the average oil or to the top oil temperature, except that the latter values cling more closely to the curve $n = 1$.

Very frequently, a whole heating transient can be approximated accurately by the theoretical curve for $n = 1$, as shown by the example given in Figure 4 of the discussion. In addition to this, the initial slope of all heating transients in Figure 3 of the discussion is the same, and so is the initial slope of all cooling transients. These slopes are independent of n since they represent the condition that no heat is dissipated. Up to the time values of $t/t_h = t/t_c \approx 0.4$, the heating and cooling curves, Figure 3 of the discussion, run sufficiently close together so that, practically, it will not make any difference what value of n is used to interpret the test results. Consequently, the functions obtained for $n = 1$, which are the simplest ones, will serve this purpose. On the other hand, the test results of those initial portions of the heating and cooling curves constitute, for example, sufficient information for an extrapolation back to shutdown and for computing the thermal capacitance and the cooling-time constant of the winding.

For all these reasons, the authors have abandoned for some time all efforts to use a heat dissipation exponent other than $n = 1$, when evaluating heating or cooling curves. Montsinger seems to have come to a similar conclusion, since he recommends the extrapolation of cooling curves by plotting them on semilogarithmic paper. In such a coordinate system, the test curves of the differentials appear as straight lines only if they can be represented by equation 1 of the paper, which is Montsinger's formula for $n = 1$.

This method suggested by Montsinger will give more satisfactory results if the following modifications are introduced:

Instead of plotting resistance increments corresponding to temperature differentials which are the winding rise over an average of the oil temperature with respect to time, plot the temperature differentials, D, which are the winding rise over the actual variable oil temperature. Since the oil rise, θ, can easily be read by the use of thermocouples during the entire cooling period under consideration, there is no need to rely on an average value of θ only. Extrapolate the values of D on the semilogarithmic paper back to D_0 at shutdown, and lay a tangent to this experimental curve at the point D_0, as shown in Figure 5 of the discussion, for the example already introduced in Figures 1 and 3 of the discussion. The time Δt at which the ordinate D of that tangent line becomes equal to $D_0/(e = 2.71)$, is the cooling-time constant t_c. Then proceed to find the values of D_F, t_h, and k, as shown in connection with equations 8 to 11 of the paper.

In order to improve the accuracy of this method, take as many readings as possible within a time which is only a small fraction of t_c. This procedure is illustrated by the example, Figure 6 of the discussion, where a time $\Delta t = t_c/6$ is reached when D has dropped to $D_0/e^{1/6}$.

The problem of evaluating transient heating curves is somewhat different from that of analyzing cooling curves. In overload tests, the power is frequently interrupted before the differential reaches its steady value D_F in order not to overheat the windings. Actually, because of the increase of the losses with temperature, such steady differentials are obtained only when the oil rise becomes steady too. It is often impossible to continue the load that long without destroying the unit. On the other hand, the upper part of a heating curve, of the type shown in Figure 1 of the discussion, does not represent a simple mathematical function, so that only the portion up to the neighborhood of the first maximum or turning point can be approximated by equation 3 of the paper. In any case, the quantity D_F represents a fictitious final differential, introduced in the paper solely for the mathematical analysis of the test values; that is, D_F does not appear among these values but has to be computed from them.

In general, it is desirable to evaluate the tested curves in such a way that the results can be used to precalculate the heating transients for other load conditions. For this purpose, find not only the final differential D_F, but also the time constant t_h and the initial slope (in degrees centigrade per minute)

$$r_s = \frac{D_F}{t_h} \tag{5}$$

of the heating transient. The slope r_s will be of interest since it furnishes the thermal capacity, k, according to equation 9 of the paper in the form

$$k = \frac{W_x}{r_s} \tag{6}$$

If a good estimate for t_h can be obtained by plotting the whole test curve, the value of r_s may be computed by transforming the test points D into a straight line with points D_x, as shown in Figure 4 of the discussion. The procedure is the same as derived in the paper for the evaluation of cooling curves, if the temperature drop ΔD, and the time after shutdown Δt, are replaced by D and t in formula 7 of the paper and in Figure 7 of the paper. The quantity $\Delta D_x = D_x - D$ will then again represent the correction between the actual curve and the straight line points. Note how closely the converted test values at the start of the heating and cooling curves are approximated by straight lines so that the use of a heat dissipation exponent $n = 1$ appears quite justified in this example.

From these lines, the values of $k = 5.93$ and 5.65 have been computed according to the equations 8 to 11 of the paper and equation 5 of the discussion for the beginning and for the end of the heating cycle.

In order to reduce the numerical work in the analysis of tested heating transients, a second method has been developed in which these curves are approximated by equation 3 of the paper so that two test points D_1, t_1 and D_2, t_2 coincide with this mathematical curve, as shown schematically in Figure 7 of the discussion. This approximation is even better than the conversion into a straight line, since the upper part of the transient is included. For the analysis, introduce the ratio

$$a = t_2/t_1 \tag{7}$$

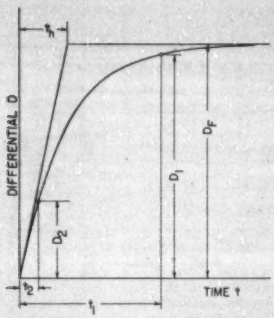


Figure 7 (left). Schematic diagram of the evaluation of heating transients by using two test points

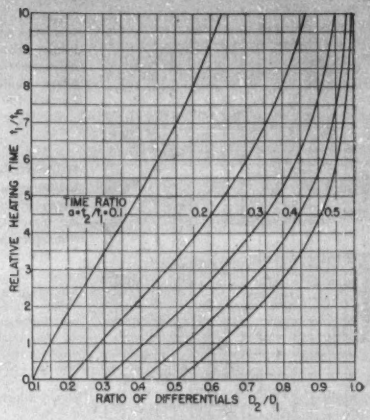


Figure 8 (right). Chart for the evaluation of heating transients by using two test points

and find, according to equation 3 of the paper,

$$\frac{D_2}{D_1} = \frac{1-e^{-a\frac{t_1}{t_h}}}{1-e^{-t_1/t_h}} \tag{8}$$

The values of D_2/D_1 can be computed by this equation for any given amounts of a and t_1/t_h. In this way, t_1/t_h has been found as a function of D_2/D_1, and is plotted in Figure 8 of the discussion for the constant ratios of $a=0.1$, 0.2, 0.3, 0.4, and 0.5. In order to find the constants t_h and D_F by the help of this graph, select two values t_1 and t_2 of a preferably low ratio a. For the corresponding test values D_1 and D_2 and for their ratio D_2/D_1, take t_1/t_h from Figure 8 of the discussion and find t_h. For $t/t_h=t_1/t_h$, a quantity D/D_F can be taken from Figure 5 of the paper, furnishing D_F.

As an example, consider the transient heating curve of Figure 1 of the discussion, and select $t_1=26$ and $D_1=103.5$ at the turning point. Choose $a=0.1$, and find $t_2=2.6$, $D_2=21.4$, that is, $D_2/D_1=0.207$, which furnishes $t_1/t_h=1.96$ in Figure 8 of the discussion, and so $D_1/D_F=0.857$ in Figure 5 of the paper. Consequently, obtain $t_h=13.3$, $D_F=121.0$.

Compute $r_s=9.1$, according to equation 5 of the discussion, and $W_s=41.0$ from the current density in the winding, and find $k=4.40$ by equation 6 of the discussion.

The accuracy of the constants D_F and t_h, which have been obtained in this way from the experimental heating curve, may be checked on the basis that the quantity D_F-D of a heating curve, given by equation 3 of the paper, has the same analytic form as the differential D of a cooling curve when represented by equation 1 of the paper. Consequently, the initial part of a tested heating curve will approximate a straight line and can be analysed in the same way as a cooling curve, if such a heating curve is plotted on semilogarithmic paper in terms of D_F-D against t. The heating curve of Figure 1 of the discussion has been added in this way to Figure 5 of the discussion. Lay an initial tangent line to this curve, and find $t=t_h=13.7$ where this line reaches $D_F-D=D_F/e$. Use this new value of t_h, that is, $t_1/t_h=1.90$, and find a better approximation $D_1/D_F=0.848$ and $D_F=122$ by the help of Figure 5 of the paper. If a higher accuracy is desired, read, as in Figure 6 of the discussion, a quantity $t=t_h/6$ at $D_F-D=D_F/e^{1/6}$ by securing a sufficient number of test points in the lowest part of the heating transient.

The examples presented above should give sufficient proof that it is satisfactory to base the evaluation of heating and cooling curves on a proportionality between the differentials and the losses in the winding, as considered in the paper. Consequently, it is not necessary to complicate the analysis of the test data by resorting to Montsinger's formula with an exponent of heat dissipation other than $n=1$.

In addition to this, the authors show also how the experimental results, obtained by a heat run under load, can be evaluated for future use, and that the values thus found are much more accurate than those furnished by other methods.

Experience with Transformer Impulse Failure Detection Methods

L. C. AICHER
MEMBER AIEE

COMMERCIAL impulse tests have been performed for some 16 years. During this period the tests have been conducted in various manners depending upon the rules in effect at the time. Periodically the rules are amended to incorporate improvements in the technique of testing. It is a purpose of this paper to show how excitation during an impulse test complicates the test and confuses the interpretation of results. Experiences with testing techniques that promise improvement are related.

Impulse Testing in General

It is interesting to review the prevalence of impulse testing as experienced by one manufacturer. The fact that a larger percentage of the transformers manufactured are now tested, Figure 1, is only a part of the story. When today's volume of production is considered, it means a manifold increase in number of units tested.

The very nature of impulse testing makes it a time-consuming operation. It is then not unrealistic to review impulse testing practice in search of suggestions that will save time. Another desire of equal importance is for methods that lessen the possibility of a failure going undetected. Much time is consumed checking suspicious oscillograms to make sure no failures go unnoticed.

Most everything has been done, consistent with the proved ability of the equipment, to simplify the test program. The American Standard C-57[1] has been newly amended to allow recently developed techniques.

Transformer failures during impulse test, arranged according to simplicity of detection, can be classified as follows:

1. Winding-to-ground involving major insulation.
2. Winding-to-winding.
3. Across relatively large portions of the winding but not involving ground.
4. Between coils.
5. Between coil face and taps.
6. Between turns.

As is stated in ASA Standard C-57,[1] "—one of the most important matters is the detection of failure." It is also stated that indications of insulation failure include: "noise within the transformer; presence of smoke; excessive current or drop in voltage in the excitation circuit as indicated by the magnetic oscillograph; failure of a gap or bushing to flashover although the oscillogram indicates a chopped wave; and presence of oscillations or other variations from the expected wave shape as indicated by cathode-ray oscillograms."

A discussion of the individual indicators listed has appeared elsewhere in other literature.[2] Experience has shown that the presence of any of these is nearly positive proof of failure. However, it is insufficient to say that their absence is proof there has been no failure.

Improvements in impulse testing technique develop more rapidly than do standards. A certain period of proving is necessary before standards are amended, yet it is difficult to gain wide experience with a new technique until it is permitted by the standards. Many of the oscillograms in this paper were obtained during tests on experimental units while others are from units given commercial impulse tests.

Tests with Excitation

Several manufacturers have advocated the elimination of excitation during impulse tests.[2,3,4] The argument is based upon experience obtained with the test from the inception of recognized testing rules.

It is not essential here to discuss the reasoning behind excitation. The important point is that it has failed to do its job. The record shows that current does not follow the impulse failure path except under special circumstances. Furthermore, the complications of the test necessitate conditions that confuse the interpretation of results.

If a failure is suspected while testing with excitation, it is an axiom to eliminate excitation, drop all the exciting equipment from the circuit, and concentrate on the winding under test. This eliminates all the protective devices from windings not being tested, as well as isolating gaps in the impulse generator circuit. A highly sensitive resistance-potential divider can then be used instead of a capacitance-potential divider that is more subject to extraneous oscillations. Only then is there reasonable assurance that the disturbance is confined to the transformer.

A simplified diagram illustrating the impulse generator circuit connected to a transformer which is to be excited during tests is shown in Figure 2. The components C_1, L_1, R_1, R_3, and C_2 represent the effective parameters in the impulse generator circuit during discharge. Capacitors C_3 and C_4 in series form a voltage divider for measurement of the applied surge. The rod gap used to produce a chopped wave is identified by *RG*. An insulating and voltage ratio transformer is necessary to provide the proper excitation voltage to the transformer under test and to act as a buffer to prevent dangerous surges from reaching the source of excitation. In order that excitation volt-

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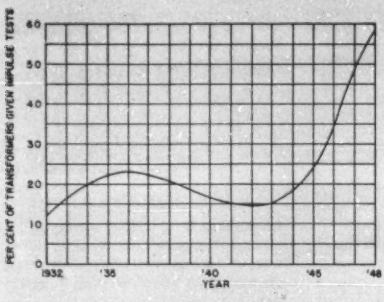
L. C. AICHER is with the Allis-Chalmers Manufacturing Company, Milwaukee, Wis.



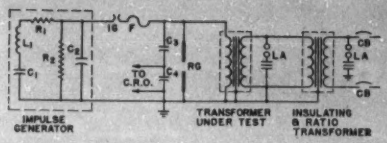


Figure 1 (left). Trend of impulse testing of power transformer

Figure 2 (right). Simplified impulse generator discharge circuit with normal frequency excitation

age does not flow through portions of the impulse generator, an isolating gap, *IG*, must be provided. A fuse, *F*, must be provided to interrupt the flow of excitation current into the generator after the impulse has crossed the isolating gap on its way to the transformer terminals.

For the moment, consider the circuit of Figure 2, but without the two lightning arresters shown connected to the transformers. If such a circuit is used during an impulse test, a bushing flashover on a winding other than the one being tested probably will occur. Such a disturbance is shown in Figure 3A, at approximately 19 microseconds, obtained while applying impulse voltage to the high-voltage line bushing during which a low-voltage line bushing flashed over. Figure 3A is interesting as a tap failure is indicated at about 2 microseconds. Imagine the difficulty of recognizing the existence of a failure had the time interval between the failure and the bushing flashover been nearly zero.

Three lightning arresters were applied to the excitation circuit and third winding and the oscillogram of Figure 3B was obtained. The oscillogram shows that the lightning arresters have operated to prevent the bushing from flashing over, thereby reducing the magnitude of the disturbance on the tail of the wave. However, it is evident that the identification of a failure has been complicated by the introduction of additional disturbance produced by the necessary lightning arresters. Additional illustrations of the effect of lightning arresters operating are shown in Figures 4, 5, and 6.

Proof that the disturbances observed in Figure 7A are due to the lightning arresters, can be developed by applying repetitive surges to the winding under test while voltage measurements are made on the terminals to which the lightning arresters are connected. The oscillograms of Figure 7 were obtained on the same transformer as those of Figure 6, with the exception that those of Figure 7 were measured with a resistance-potential divider directly connected to the transformer, instead of a capacitance-potential divider. This test arrangement involved two lightning arresters. The lightning arrester operations are reflected into the applied voltage wave, Figure 7A, one at about 10 microseconds and the other at about 37 microseconds. The time at which lightning arresters discharge when connected to unsurged windings is governed by the shape and magnitude of the induced voltage wave of that winding. Figure 7B is of the voltage wave obtained on the low-voltage winding with the surge applied to the high-voltage winding, and verifies that the lightning arrester operation occurring at approximately 37 microseconds in Figure 7A is the lightning arrester on the low-voltage terminal. In a similar manner Figure 7C verifies that the disturbance at approximately 10 microseconds is due to the lightning arrester connected to the tertiary-voltage winding. The three oscillograms of Figure 7 are unusually consistent as to time of lightning arrester discharge.

Figure 8 shows four oscillograms obtained with successive applications of voltage upon a transformer. Note the variation in time to discharge as well as magnitude of disturbance produced by the lightning arresters. These oscillograms were obtained on the low-voltage winding of the transformer which produced Figures 6 and 7. Here the lightning arresters were in the high-voltage and tertiary-voltage circuit. No excitation was applied.

Upon closing the excitation circuit, a change takes place in the impedance of the load on the impulse generator such as usually to shorten the tail of the applied impulse wave. This is illustrated in Figure 9 which again is on the same transformer as Figure 8 with the addition of excitation. It is evident that the change in slope of the tail of the wave as compared with that of the reduced voltage full wave without excitation, Figure 8A, can lead to confusion during the demonstration that the transformer has withstood the test. A change in the slope of the tail of the wave could lead to an interpretation of high leakage over insulation to ground, or a change in the amount of winding that is in series with the impulse generator circuit.

Consider Figure 7C for a moment. If the applied voltage is increased the voltage induced in the other windings will increase proportionately. At the times of interest, the volt-time characteristic of the lightning arrester is flat and constant, plus or minus the manufacturing tolerances. When the applied voltage is increased from the reduced full wave level to full wave level, the time to lightning arrester operation is shortened for a voltage wave similar to Figure 7C. Since the lightning arrester discharge has occurred sooner, the reduction in length of wave tail starts earlier and results in a shorter tail than that of the reduced full wave.

Sometimes the time to a given lightning arrester operation can be controlled by adjusting the size of the lightning arrester in proportion to the magnitude of the applied voltage wave. For example if a 120-kv lightning arrester was normal for a given winding then during the reduced full wave test, applied at about 60 per cent of the full wave voltage, a 72-kv lightning arrester would be used.

There seems to be no question that if the cathode-ray oscillograph is properly used it will detect any failure experienced to date. It is very important that the measuring circuit be as sensitive as it is

Figure 3. Full waves applied to a high-voltage winding

(A) Low voltage bushing flashover caused disturbance at about 19 microseconds. (B) Full wave after three lightning arresters were applied to the excitation circuit

Figure 4. Effect of lightning arresters on impulse wave

(A) Reduced full wave. (B) Full wave

Figure 5. Effect of lightning arresters on impulse wave

(A) Reduced full wave. (B) Full wave

Figure 6. Effect of lightning arresters on impulse wave

(A) Reduced full wave. (B) Full wave

humanly possible to make it. Extraneous oscillations must be sufficiently small as not to affect the intrepretation of the results. They cannot be filtered out by devices inserted into the circuit for such devices will filter likewise similar minute disturbances which may be caused by turn-to-turn failures in the transformer.

The cathode-ray oscillograph has detected failures where the 60-cycle excitation that was present offered no evidence. There is no question that the oscillograph will detect a failure from the line end of the winding to ground as represented by Figure 10. An example which involves the failure of 10.2 per cent of the winding between coils somewhat removed from the line end is shown in Figure 11, (A) the reduced voltage full wave and (B) the full wave which followed two chopped waves.

The tap failure pointed out in Figure 3 was in a section of winding extending beyond the grounded point and was removed as far from the point of measurement as is possible, yet the cathode-ray oscillograph indicated the failure.

Probably the most difficult type of failure to detect is a turn-to-turn fault. Such failures are most likely to occur during the application of a steep-front impulse test. This is because of the increased magnitude of voltage applied and the fact that turn-to-turn insulation has very little additional inherent strength at short times. Even so, the cathode-ray oscillograph has indicated turn-to-turn failures involving less that 1/10 of 1 per cent of winding. Figure 12 is an example in which considerable difference is noted after the steep-front wave has chopped to zero. The inability to match the oscillations about the zero line when the oscillograms are superimposed is as indication of failure. It is also possible that failure can occur during the rising part of the steep wave test.

Tests Without Excitation

After some eight years of impulse testing with excitation, and finding that excitation only added complications to the test, some testing was carried out without excitation. Fewer lightning arresters or other protective devices were used to control the flashover of bushings on the windings other than the one being tested. This did not eliminate the disturbances produced by the protective devices but it did reduce their number and simplified the preliminaries to a test which further reduces the complications that arise when a failure is suspected.

It is undesirable to stress any winding not being tested, particularly one on which there is no measuring equipment. It is reasonable to consider methods which will protect these windings and at the same time not produce the disturbances associated with some protective devices. Upon eliminating excitation, practical use can be made of linear resistors as protective devices. In many instances, the winding not being tested can be short circuited and grounded. Obviously this protects the winding and prevents bushing flashover.

On first thought this would seem to be a most impractical method of test because it does not approach service conditions. An analysis of what takes place within a transformer during an impulse test will soon show that at the initial instant when maximum stresses are usually obtained between turns and from coil-to-coil near the line end of the winding, conditions with this connection are representative. After some interval of time during which surge flows through the winding, a finite value of inductance exists and the winding under test will oscillate at a frequency depending upon the natural period determined by the capacitance and inductance components of the winding. When the other windings are solidly connected to ground, a maximum voltage is developed from the winding under test to all other windings. This is true because the induced voltage in the other winding is held to a minimum, thereby increasing the differential of voltage between the winding under test and those nearer to ground potential. The midpoint of the other winding will not remain at ground potential, but will rise to values above ground and will oscillate at frequencies determined by the characteristics of the winding. When so tested the magnitude of the voltage will be less than when each winding receives its appropriate impulse test. Such a test procedure is used on development work and on those commercial units where it is applicable.

Frequently it is not possible to maintain the nominal 40 microsecond length of tail during an impulse test. An answer is to provide an impulse generator of suffi-

Figure 7. Verification of lightning arrester activity

(A) Reduced full wave applied to high-voltage line terminal. Note lightning arrester activity at about 10 and 37 microseconds. (B) Induced voltage in low-voltage winding showing operation of low-voltage lightning arrester. (C) Induced voltage in tertiary-voltage winding showing operation of tertiary-voltage lightning arrester

Figure 8. Oscillograms of successive voltage applications showing variation in time to lightning arrester operation. No excitation

Figure 9. Effect of excitation circuit on length of tail (compare with Figure 8)

Figure 10. Line-end to ground failure during full wave

cient energy output to produce the wave shape. This solution, however, is not always practical. It is possible to supply so much energy into a fault that the disturbances do not show in the oscillogram. This suggests that less emphasis on the length of wave tail is in order in favor of more accurate measurements. If the unsurged windings are protected by a grounded short circuit between terminals, a short tail may result. The short tail of the applied wave can be raised somewhat by connecting a suitable resistance between the winding terminals instead of the solid short circuit. One terminal only is then grounded. In other cases, resistance between the grounded terminal of the winding under test and ground is a desirable aid in raising a short tail. There is no cure-all. Each situation depends on the characteristics of the transformer.

The length of the tail of the impulse test wave as specified in ASA C-57 is rather arbitrary and primarily so defined for standardization purposes. The standards recognize that conditions can exist under which the nominal length of tail cannot be obtained. The length of the tail of the applied impulse wave influences the voltage from the winding to ground at points removed from the line end. These voltages are not determined by the turns ratio of the point in question in the winding, but rather by the natural periods of oscillations in the winding. A point often overlooked is that if conditions are such as to produce a short tail during an impulse test the same low impedance will dissipate the energy of a field lightning stroke in a correspondingly rapid fashion.

Neutral Current Measurement

Since the cathode-ray oscillograph has proved to be a very dependable device when properly used, it is not surprising that additional uses of the device have been suggested for failure detection. A method of measuring the current at the grounded terminal of the winding under test was suggested as a means of failure detection.[2] The procedure is to insert a shunt between the terminal and ground and apply the voltage developed across the shunt to the plates of an oscillograph. A recording would be made at reduced full wave level and at full wave level. It is highly desirable that the potentiometer ratios be adjusted so the physical amplitude of the oscillograms is comparable then they can be superimposed and easily studied for differences. Usually these neutral current oscillograms will consist of many frequencies and a mismatch is indicative of a change in the transformer's impedance such as when a failure occurs. Small changes in impedance result in larger changes in the neutral current oscillograms than occur in the voltage oscillograms taken at the surged terminal.

Experience has shown that the characteristics of the shunt are important. Its resistance must be as small as possible yet it must produce a suitable voltage for deflection of the oscillograph trace. If the resistance of this shunt becomes too large it affects the balance of surge currents in the transformer and may even cause flashovers of the grounded bushing. It can materially change the magnitude of oscillations in the winding under test. This is undesirable.

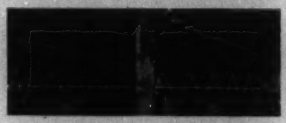


Figure 11. Coil-to-coil failure involving 10.2 per cent of winding

(A) Reduced full wave. (B) Full wave with excitation

Figure 12. Steep-front oscillograms showing failure

Figure 13. Full wave and neutral current oscillograms of a high-voltage winding

(A) Reduced full wave. (B) Full wave. (C) Reduced neutral current. (D) Full neutral current

Figure 14. Neutral current oscillograms of a low voltage winding

(A) Reduced neutral current. (B) Full neutral current

The writer's initial experience with this neutral current measurement is a rather striking example of what can be expected from this test. Figure 13A is the reduced voltage full wave obtained prior to the application of the steep waves shown in Figure 12. It was recognized that the oscillograms of Figure 12 indicated failure during the steep wave test. This is borne out further by the inability of Figure 13B to match Figure 13A. Unfortunately the potential divider ratio for these full wave oscillograms was not adjusted so as to give more nearly equal deflection of the records for comparison purposes. Figure 13C is the current neutral measurement obtained when a reduced voltage full wave was applied and Figure 13D is the current neutral measurement obtained during the full wave test. It is quite obvious that these current measurements will not superimpose. As mentioned earlier this was a turn-to-turn failure involving

no more than 1/10 per cent of the winding. It is worthy of mention that not all neutral current waves are as slow to reach their crest as are Figures 13C and 13D. This is illustrated in the reduced current measurement and full current measurement of Figure 14. The current measurements of Figure 13 are from a high-voltage winding whereas those of Figure 14 are from the low-voltage winding of the same transformer.

Insufficient wall thickness of insulation in critical regions may not lead to a major failure during impulse tests, but, excessive corona can exist and if sufficient number of transients are applied failure probably would result. The oscillograms of Figure 15 strengthen the case for the neutral current measurement. Upon examination of Figures 15A and 15B, one with limited experience could easily say the transformer has satisfactorily passed the impulse test. The critic certainly would recognize the differences when shown Figures 15C and 15D, the reduced current wave and full current wave respectively. In this instance, as has been suggested, a portion of a live part directly connected to the line terminal to which the surge voltage was applied was deficient in insulation-wall thickness. Sufficient corona existed to mark the insulation although failure had not taken place.

After the test which produced the series of oscillograms of Figure 15, two questions were raised, "Is not the current neutral method of failure detection too sensitive? Is it not a method of detection more sensitive than our ability to manufacture on a commercial scale?" An answer to these questions was obtained by testing typical major insulation barriers using a shunt between the test tank and ground. It was

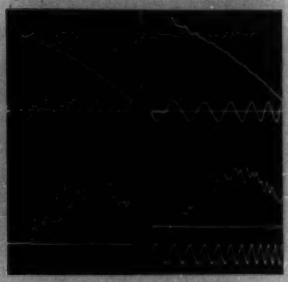


Figure 15. Corona discharge detection with neutral current measurement

(A) Reduced full wave. (B) Full wave. (C) Reduced neutral current. (D) Full neutral current

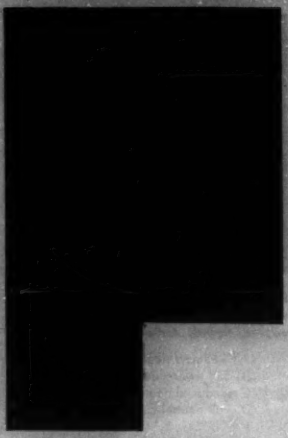


Figure 16. Correlation of voltage and neutral current oscillograms of a test barrier

(A) Maximum full wave before failure. (B) Neutral current corresponding to (A). (C) Neutral current similar to (B) except with a faster sweep. (D) Voltage oscillogram of barrier failure showing corona discharge before failure. (E) Neutral current oscillogram of barrier failure. (F) Oscillogram similar to (E) except with a faster sweep. (G) Neutral current oscillogram showing corona discharge but no failure

necessary to take a voltage wave then a current wave and continue this alternating arrangement throughout the test.

The impulse voltage on the barrier was raised to the maximum value possible before injurious discharges occurred. A voltage oscillogram of this wave is Figure 16A. The neutral current corresponding with the full wave oscillogram is in Figure 16B. A similar neutral current recorded with a faster sweep is illustrated in Figure 16C. Upon raising the voltage a small increment failure was produced as shown in the voltage oscillogram of Figure 16D. It is noted that a dip after the crest of the wave has occurred similar to those associated with the existence of corona discharge just prior to failure. The current neutral measurement corresponding with this failure is shown in Figure 16E which when expanded on a faster sweep is illustrated in Figures 16F and 16G. The conclusion drawn from this series of tests is that the current neutral method of measurement is no more sensitive than the voltage measurement, but is more positive because the magnitude of the recorded disturbance is greater. This is based upon the evidence shown here that both have indicated the existence of corona before breakdown.

An interesting example of the behavior of the current neutral measurement during a tap failure involving 9 per cent of the winding is shown in Figure 17. The taps were within a section of approximately 18 per cent of winding that was tapped out during the test. The failure occurred during steep wave tests. It was evident on the steep wave oscillograms, however, on the following chopped waves and full waves no evidence appeared on the oscillograms. Bubbles appeared following the full wave during which the current measurement was made.

Very often a failure in one winding will reflect itself into the oscillogram during tests on another winding. Such a situation was studied using the current neutral method of measurement in an effort to observe its behavior under these conditions. The transformer studied was a 3-winding unit in which a turn-to-turn failure involving 0.85 per cent of the winding existed in the tertiary winding. There is a slight difference between the reduced full wave and the final full wave as indicated by Figures 18A and 18B. Upon comparing the reduced current measurement 18C

Figure 17. Neutral current oscillograms showing tap failure involving 9 per cent of winding

(A) Reduced neutral current. (B) Full neutral current

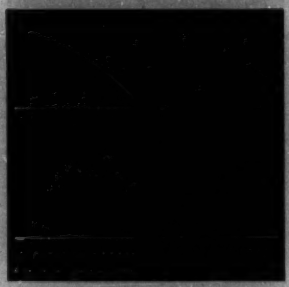


Figure 18. Oscillograms of turn-to-turn failure involving 0.85 per cent of winding

(A) Reduced full wave. (B) Full wave. (C) Reduced neutral current. (D) Full neutral current

with the final current measurement 18D, considerable difference is noticed. These oscillograms were obtained during the application of the impulse voltage to the high-voltage line terminal. The current measurements were made at the high-voltage neutral. The oscillograms of Figure 19 were obtained by applying the impulse voltages to the line terminal of the high-voltage winding with the neutral solidly grounded but with the measuring shunt connected between, first the X_1 terminal and ground, Figures 19A and 19B, then the X_2 terminal and ground, Figures 19C and 19D. In each case the low-voltage winding was short circuited through the shunt and one terminal was grounded. A similar series of tests were conducted by applying surges to the high-voltage winding but measuring the current in the tertiary, first, on Y_1, Figures 20A and 20B then on Y_2, Figures 20C and 20D.

Whether the current neutral method of measurement will detect all failures in a winding obviously depends considerably upon the characteristics of the transformer. The magnitude of induced voltage in the various portions of the winding during tests on some other winding is controlled by the coupling between windings and will govern the effectiveness of measurements similar to those described in the previous paragraph. As a matter of fact, Figure 21 shows the current measurements obtained when the winding containing the failure was surged. Figures 21A and 21B are for the surge applied to

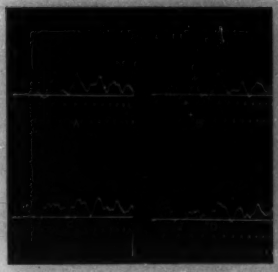


Figure 20. Neutral current measurements in windings other than the one surged. High voltage surged, tertiary voltage measured

(A) Reduced neutral current at Y_1
(B) Full neutral current at Y_1
(C) Reduced neutral current at Y_2
(D) Full neutral current at Y_2

the Y_1 end of the winding with the current neutral measurement made at the Y_2 terminal. A difference exists between these current measurements indicating failure as a probability. On the other hand, referring to Figures 21C and 21D, the current measurements obtained when the surge is applied to the Y_2 end with the measurements at the Y_1 terminal, no disturbance appears. In this instance, as pointed out previously the failure involved 0.85 per cent of the total winding and is located about 2 per cent of the winding from the Y_1 end. The results obtained are not an unreasonable situation for it must be remembered that during

Figure 22. Coupling between windings controls neutral current indication. Same transformer as Figures 19, 20, and 21. Tests and measurements on low voltage winding

(A) Reduced neutral current at X_2 when surging X_1. (B) Full neutral current at X_2 when surging X_1. (C) Reduced neutral current at X_1 when surging X_2. (D) Full neutral current at X_1 when surging X_2

the test the portion of the winding near the terminal under test experiences the most severe initial voltage stress. Oscillograms of Figure 22 are included here to illustrate further what has been stated previously regarding the effect of the de-

Figure 19. Neutral current measurements in windings other than the one surged. High voltage surged, low voltage measured

(A) Reduced neutral current at X_1
(B) Full neutral current at X_1
(C) Reduced neutral current at X_2
(D) Full neutral current at X_2

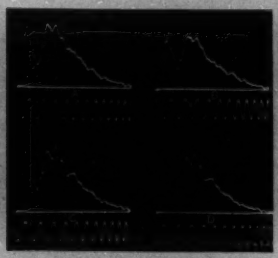


Figure 21. Location of failure within winding with neutral current measurement. Tests and measurements on winding (tertiary voltage) containing failure

(A) Reduced neutral current at Y_2 when surging Y_1. (B) Full neutral current at Y_2 when surging Y_1. (C) Reduced neutral current at Y_1 when surging Y_2. (D) Full neutral current at Y_1 when surging Y_2

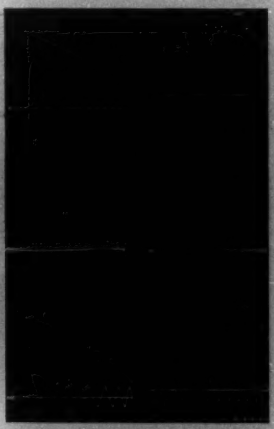


Figure 23. Neutral current measurement did not detect this failure

(A) Reduced full wave. (B) Reduced neutral current. (C) and (D) Steep-front waves (showing failure). (E) Full wave. (F) Full neutral current

gree of coupling between the various windings. The oscillograms were obtained by applying the impulse voltage to the transformer first to the X_1 terminal then to the X_2 terminal with the current measurement being made in the other terminal each time. No indication of failure is evident in either case.

Until now all of the discussion regarding the current neutral method of failure detection has indicated that it is a reasonably reliable method. But what about the other side of the story? The quantity of evidence that another and less desirable side of the story exists is not great. In fact the oscillograms in Figure 23 are the only oscillograms the writer is aware of which indicate the current neutral method may not always detect a failure. In this instance the oscillograms are arranged in the order in which the tests were applied. Two chopped waves were applied following the steep waves illustrated but since they were perfectly normal, add nothing to this discussion and have been omitted. The steep wave oscillograms, Figures 23C and 23D show a disturbance has taken place. Upon comparing oscillograms 23A and 23E it is difficult to find evidence of the disturbance. The current neutral oscillograms (B) and (F) can be superimposed with no indication of the disturbance. This is a conceivable situation assuming that the dielectric was of such strength as to almost withstand the steep wave test. It is further verification that the steep wave voltage levels are not coordinated perfectly with the $1^1/_2$ by 40 levels, otherwise a situation like this could not exist.

It should be pointed out that the experiences related here do not necessarily cover all types and designs of transformers. While several types are represented in the illustrations included in this paper it is possible that variations of these types can alter materially their response under transient conditions.

Conclusions

It is felt that while no one method of failure detection is infallable, the most desirable method is one making use of voltage oscillograms with neutral-current measurements. These tests would be made without excitation and without the addition of protective devices, except linear resistors to windings other than the one being tested unless such protective device is a permanent part of the apparatus.

For example, a by-pass lightning arrester applied to an autotransformer or feeder voltage regulator. The latest edition of ASA Standard C-57 suggests the use of the neutral current method of measurement. The standard has been amended to permit impulse tests without excitation. It is no violation of the standards therefore to conduct an impulse test in the manner suggested in this paper.

References

1. American Standards for Transformers, Regulators and Reactors, C57. American Standards Association (New York, N. Y.), 1948.

2. Progress in Impulse Testing of Transformers, J. H. Hagenguth. AIEE Transactions, volume 63, 1944, pages 999–1005.

3. First Line of Defense, L. C. Aicher. *Allis-Chalmers Electrical Review* (Milwaukee, Wis.), December 1940.

4. Transformers are Winning Fight to Lick Lightning, L. C. Aicher. *Allis-Chalmers Electrical Review* (Milwaukee, Wis.), September 1945.

Discussion

P. L. Bellaschi (Consulting Engineer, Sharon, Pa.): We have barely completed the second revision of the Transformer Standards, ASA-C57,[1] when we find ourselves discussing a revision of the new standard. But this naturally is the course of modern standardization; a good standard is only a step to a better standard.

In the transformer test code, the section on impulse failure detection methods, no doubt, is an important one, for it has a bearing on the integrity of the transformer delivered to the user; it affects the test equipment involved; and, in fact, it influences the whole economy of testing, time consumed, and, believe it or not, the delivery date of the transformer tested. Therefore, it is proper that we take stock from time to time of the experience and developments in this field, as indicated in the paper.

One could very well take issue with some of the statements in the paper but for the purpose here we do know that the neutral-current method of detection, which consists in measuring the surge current at the neutral end or grounded terminal or in a separate winding, has shown distinctive merits and can well be considered seasoned by experience at this stage. In combination with the voltage oscillograms of the terminal tested, it permits looking into the winding from the two ends, so to speak, and with a much clearer view of what is going on inside.

To my knowledge, wherever the method has received a fair trial it has shown its intrinsic merits. According to my observations, even cases of secondary faults involving corona or minor discharges across a tap changer or similar effects, which might well have escaped detection by ordinary methods, have been picked up clearly by the neutral-current method. Judging from some of the minor disturbances detected, one might, perhaps, be tempted to the statement that the method is too sensitive. There have been a few cases, too, where reliance had to be placed entirely on the voltage oscillograph as well as the other usual observations. On the strength of present-day experience it would then be rather bold to claim infallibility for the method.

In a future revision of the Transformer Standards, the neutral-current method, no doubt, should be given greater prominence, but on general principles, it would be a mistake to exclude other possible useful methods. So far, acoustical methods have been tried out only to a limited extent; certainly, these too should be explored. How about chemical methods of detection? As a matter of fact, the attention given hereto to the neutral-current method has been almost entirely from the experimental approach. Very little has been done to analyze and rationalize the method.

A word or two in regard to international standards on transformers. These were discussed in a measure before the International Conference on High-Voltage, Electric Systems and meetings of the International Electrotechnical Commission held in Paris in the summer of 1948. In addition to attending these meetings, the writer had occasion to visit the various electrical manufacturers in Sweden and England, and also in Switzerland and Italy to find that impulse testing of transformers in a few of these countries has been making decided progress in recent years. For instance, a number of the 230-kv power transformers recently installed in Sweden have been impulse tested, practically at our levels, 1,025 kv. While the volume of power transformers tested abroad may not be as large as ours, rather intense research has been directed to the impulse failure detection methods. I feel quite confident that when it comes to deciding on an impulse test code for transformers in international standards, the neutral-current method of detection is due for a high vote.

In my opinion, the present transformer standards were drafted wisely. They permit testing by the neutral-current method. And in conclusion I would suggest that, due recognition be given to the neutral-current method in a future revision of the Transformer Standards, but that these be drafted not to impede future progress and developments. In the field of international standards, there is now reasonably good common ground for agreement on the matter of transformer impulse test methods.

References

1. See reference 1 of the paper.

F. J. Vogel (Illinois Institute of Technology Chicago, Ill.): The general subject of impulse testing for transformers is up for review. Aicher's paper is therefore both timely and good. There are several statements in the paper which I do not agree with, but also to enumerate them and discuss them would serve no useful purpose. I would like to point out some considerations which are now being taken into account by

a working group of the transformer committee regarding impulse testing.

It is felt that impulse testing is on the increase. I think that Aicher, and many other manufacturing and test engineers, would insist on such tests if they became responsible for purchasing or operating transformers. All manufacturers and test engineers agree that impulse testing with 60-cycle excitation has difficulties, and it would be desirable from this standpoint if such excitation were not required. The omission of 60-cycle excitation as a requirement has been proposed and probably will be recommended.

I feel that the detection problem is unduly accentuated. The use of oscillographs and choke coils for determining failures in insulation at 60 cycles is used for radio equipment but is not required for power transformers. Why not? Transformers were made for years to meet 60-cycle tests which punctured insulation and formed carbon paths, and still could be made that way so far as the standards are concerned. It is not necessary to change the standards because the specified tests alone determine the general level. Sixty-cycle excitation was in part required as it made impulse test conditions more similar to operating conditions and stresses from one winding were transmitted into other windings as in practice. However, these values might better be given up if the more general practice of impulse testing can be aided. Estimates of the loss in test severity on omitting 60-cycle excitation has been estimated to vary from practically nothing to around 14 per cent depending on the design and present levels. In the actual determination of failures, it is significant that the voltage oscillogram tells the story in almost every case. The author has seen several cases where failures occurred in other windings than the one being tested where the voltage oscillogram did not tell the story, and where 60 cycle follow did. Such cases are seldom, and if the impulse tests with voltage oscillograms only had caught 95 out of 100 cases of defective materials, workmanship, and design, I believe it would have been worthwhile. Even current neutral measurements would not have added greatly, since Aicher's paper indicates it is not necessarily more sensitive, but more apparent, and failed entirely in one case. Hence, the required use only of voltage oscillograms has been proposed.

Incidentally there is the converse point of view, several times expressed by our European contemporaries and sometimes heard here. It is that impulse testing is not desirable, because of the liability of undetected damage. I would like Aicher's opinion as to the reliability of the test. It is not possible probably to prove it, but tests of models and general development work has indicated to me that impulse tests are nearly perfect in detecting trouble and much better than 60-cycle testing. Further, I infer that a relatively large percentage of failures in commercial impulse testing practice has been experienced by some manufacturers, and I should think, from the nature of the failures, probably by all. Since impulse testing started and better protection has been afforded in service, very few service failures have occurred. This fact has been in itself used as an argument against impulse testing, inasmuch as many transformers which would have failed if they had been tested, are satisfactory in service. As I see it, this could be stated in this way by an operator or purchaser: If I have this unit impulse tested, I run a small chance of undetected damage, but a considerably larger chance exists that some defect will be found. A small percentage of the defects which are found might not cause service outage but some will. The present desire is for the greatest reliability possible. To obtain this, impulse tests should be made. To further bolster this argument, I know of several cases of failures where the units had not been impulse tested, but none where they had.

John V. Mulcahy (Bonneville Power Administration, Pittsburgh, Pa.): Aicher's excellent paper covering his experiences in impulse testing is a valuable contribution to this subject in that it not only outlines experiences over a considerable period of time, using several methods of failure detection, but gives specific examples of these experiences that demonstrate the advantages and disadvantages of the methods under discussion.

The general conclusions reached are:

1. With the transformers excited during the impulse test, many complications are introduced, particularly by the necessity of protecting windings not under test with lightning arresters or other protective devices, whose operation during the test may obscure failure indications on the oscillograms.

2. Neutral current measurements will give a more positive indication of failure, where failures occur, than the voltage oscillogram in nearly every case, even where excitation is not in the picture.

3. The most reliable test conditions are, therefore, to omit the excitation and include neutral current measurements in the general test procedure.

In the experience of the members of our group witnessing impulse tests on transformers at plants of most of the transformer manufacturers in the United States, for the Bonneville Power Administration, our observations agree with the foregoing conclusions.

Our experiences parallel those outlined in the paper even to the one example shown where the current wave did not show an existing failure. We have experienced at least two such instances.

In one such case a failure occurred, with the test applied to one terminal of a 34.5 kv delta winding, that was clearly indicated on the potential wave, but not so indicated on the current wave. Curiously enough another failure occurred when the test was applied to another terminal of the same winding and again this failure showed clearly on the potential wave, but on the current wave there was only a very slight possible indication. In this latter case the failure finally showed on the current oscillogram after two additional chopped waves were applied to the winding.

On the other hand, we have seen a test where without excitation on the transformer the indication on the potential wave (a very slight dropping of the tail) could only be identified after the current wave had clearly showed the failure.

The net conclusion that we have reached is that without the complication introduced by excitation, that with enough experience nearly every failure is detectable on the voltage wave, but that the confirmation attained by the current wave is an exceptionally valuable tool for use in the test. In this respect, I agree with Aicher that the current wave is not more sensitive, but is more positive in a great many instances.

All of this would tend to indicate that we believe the elimination of excitation from the test to be extremely advisable from a buyer's standpoint. This is not the case.

In Teague's discussion of this paper he pointed out that the addition of excitation during the test added stress to the winding, at some point beyond the line end, varying from 0 to 15 per cent. In my own calculation on various designs, I have found that this additional stress might be as high as 20 per cent.

It would seem to follow, therefore, that if additional stress is present then additional insulation margin is provided by the designer to withstand that stress. It would also seem to follow that if the designer knows that this additional stress will not be present during test, then he need not provide for it, unless his over-all margin is so small that he dare not take advantage of this situation. Of course, where the calculated excitation stress is small, as it is in some designs, then this would not be an important factor.

In any event, following the thought outlined before, the elimination of the excitation is a step in the direction of lowering the test standards.

This fact was recognized by Gross in his discussion of the paper, and suggested that perhaps the impulse test level be raised correspondingly. If this would be done, I believe that it would compensate satisfactorily for the reduced stress; but I am inclined to doubt that the manufacturers will be willing to increase the major test level within the respective insulation levels.

As an alternate suggestion, I believe a combination test would be in order. A way to achieve the desired results would be to apply the reduced potential and current waves without excitation, the chopped waves and potential full wave with excitation and the full value current wave without excitation. However, this would introduce an undesirable time factor between the last potential and current waves, so the same result may be achieved by using excitation during the chopped waves only. This might also introduce the undesirable time factor between the chopped and full waves, but this would need to be determined by experience.

I might mention at this point that as a matter of expediency, the Bonneville Power Administration transformer specifications have for some time permitted the option of testing with or without excitation if neutral current measurements are made. I should also mention that we do not use steep front tests, but only the standard full and chopped wave tests. Further studies may indicate the desirability of again revising this specification.

This step that indicates a lowering of the test standard is of some concern because there seems to be some feeling among the manufacturers that the standards should be lowered still further based on the fact that field failures are considerably less than factory failures on a percentage basis. This is further based on the fact that the tests, as applied in the factory, are much more severe than actual "service" conditions.

It is our present opinion that the dis-

crepancy between field and factory statistics may well indicate that basic insulation levels are too high, rather than test levels. The practice of ordering transformers with insulation levels one or more classes lower than the service on which they are going to be used, is already with us. If at the same time test levels should be lowered, before sufficient experience with the lower insulation levels is obtained, we are likely to be courting major operating difficulties that might reach disasterous proportions.

In his paper, the author refers to the fact that the excitation was provided to approach service conditions, but concludes that this is not essential.

At the present time there are opinions to the effect that chopped waves are too severe in factory tests because they exceed service conditions.

I rather deplore the use of the term "service conditions" when applied to factory tests. When equipment is tested in the factory, it is brand new in every respect. If the test applied then is of a severity only equal to service conditions, then what assurance does the user have that when the equipment has seen some years of service that it will continue to meet service conditions.

The test in the factory should be considered as a "norm" only; a standard to be met when the equipment is new that will insure the reliability of the equipment over its expected life. If the tests as now applied are too severe, then the manufacturer is justified in applying for relief; but only after it is definitely proved that the tests are too severe. If on the other hand, the situation is that the user has been buying too much insulation, he is justified in reducing his cost, but also be assured that what he has purchased is entirely reliable in all respects. Until this situation is definitely clarified, any step to lower test standards, even the small one of omitting excitation from impulse tests, is not justified, unless otherwise compensated for.

In summing up the foregoing observations, we agree that it is extremely desirable to eliminate during impulse testing all conditions that will tend to obscure faults that occur during test and to use the most modern methods for detecting failures. The manufacturers are to be congratulated for their zeal in developing impulse testing and failure detection methods to the high plane on which they now stand. At the same time it is undesirable to eliminate from the tests any condition tending to lower the standard of the tests whereby faults now uncovered by new detection methods are prevented from occurring only by virtue of a less severe test.

A. F. Rohlfs (General Electric Company, Pittsfield, Mass.): Aicher's objections to the application of 60-cycle excitation during commercial impulse tests are well founded. As a failure detection method it is undependable, and as Aicher points out, merely adds confusion to a much more reliable method, the cathode-ray oscillogram. Therefore, it seems logical that wherever possible 60-cycle excitation should be omitted so that the more refined method can be efficiently employed.

Aicher interprets the mismatching of the oscillations following "chop off" on the steep-front waves as an indication of failure. One must be very cautious in interpreting such discrepancies. The oscillations are determined by the various circuit loops to which the rod gap is common. Changes in the oscillations from one wave to another indicate that somehow the characteristics of at least one of these loops have changed, but this does not mean that the change must be in the transformer. Steep-front tests are accompanied by currents of the order of thousands of amperes with rates of change of ten thousands of amperes per microsecond. As a result, high voltage gradients are set up along the circuit leads, comprising the grounding and high-voltage networks. The grounding network may be extensive since accurate steep-front measurements require a single good ground, at the transformer, while the lower end of the impulse generator is allowed to "ride up." Thus, high voltages appear between portions of the grounding network and building ground so that it is possible to develop corona discharges in the ground side of some of the circuit loops. Such corona discharges are generally erratic and will distort the oscillations following "chop off" in different ways on successive waves.

Another factor which influences these oscillations is the time at which "chop off" occurs. Tests made with chopped waves have shown that in order to have the oscillations match precisely, the "chop off" times of the waves must be the same. Likewise, it is highly probable that if the "chop off" times of the steep-front waves are not exactly consistent, variations in oscillations may result. Both of these factors must be considered and investigated before a decision of failure is reached.

The case which Aicher cites, wherein the neutral current method did not detect a failure which occurred on a steep-front wave, Figure 23, is perfectly plausible. However, unless other indicators, such as bubbles or noise, suggested failure, the evidence of the steep-front waves themselves seems too slight to draw a conclusion of failure. Certainly it would be more than difficult to reject a transformer on such evidence.

On the other hand, if failure did occur on the steep-front wave, the chances for it to recur on the chopped and full waves would be greatly improved if these waves were applied directly after the steep-front waves so that the failure had only a short time to "heal." Unfortunately, it generally requires many minutes to convert from the steep-front circuit to the chopped and full wave circuit so that the chances for healing are greater in this case then they are between the chopped waves and full waves where a reasonably short interval between the waves can be maintained.

The suggestion that idle windings (those not being tested) be short-circuited brings up two questions. First, the effective impedance of the transformer as viewed from the impulsed terminal will be reduced, thus increasing the current flowing through the impulsed winding. Any small current changes caused by a failure will be more difficult to see. In other words, the sensitivity of the neutral current method will be reduced much the same as the sensitivity of an ammeter is reduced by connecting a shunt across it. Second, the short-circuiting of the idle winding may decrease the voltage between the impulsed winding and idle winding, particularly where the ends of high-voltage and low-voltage windings are physically adjacent. The polarity of the transformer may be such that the end of the idle winding oscillates in opposite polarity to the applied impulse. In such a case then, short-circuiting the idle winding would reduce the voltages between windings.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The paper clearly points out the effect of 60-cycle excitation on failure detection with the attendant uncertainties introduced by protective equipment. It is also very gratifying that the author has used the neutral-current method with obviously good results. This method has been used in the General Electric test department for four years and it might be well at this time to relate some of our experience, as well as to point out precautions which are necessary to make this method successful. In the following discussion, it is understood that simultaneous 60-cycle excitation is omitted and that comparison of reduced full wave and final full wave is the criterion of failure.

1. Generally speaking, most failures occurring during impulse tests on power transformer sizes can be detected by irregularities in the voltage waves. However, from conversations and publications in the technical press, it is clear that such a comparison is a subjective method and depends on the degree of skill in observation of the testman. It is here that the neutral-current method is of value. While it is still a subjective method, the degree of departure of the current wave obtained after failure has occurred from the wave shape during reduced full wave test is many times greater than appears in the voltage waves. It becomes, therefore, much easier to recognize that failures have occurred and a greater degree of objectivity is attained.

2. There are cases, particularly in high-impedance windings, where failures across small portions of windings are definitely not shown by the voltage wave while the current wave will indicate them plainly. There are probably intermediate cases where the current wave is of great help in assisting in the detection of even rather serious failures as shown in Figure 1 of the discussion. In this case the voltage waves show very minor changes in wave shape, while the current waves leave no doubt that failure had occurred. The fault in this case was so severe that it was necessary to reduce the applied voltage to less than 50 per cent of the full wave to stop the winding from failing. The fault occurred across 3 per cent of the winding of a 5,000-kva 3-phase transformer.

3. Neither method—voltage or current oscillogram comparisons—is an absolute method unless the final full wave is applied to the transformer as rapidly as possible after the previous chopped wave, or, where used, the steep-front wave. This comes about because failures can heal themselves by

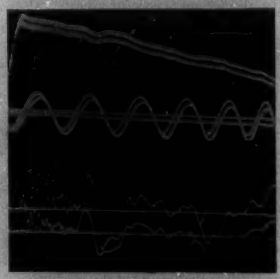


Figure 1

filling the broken-down space with new, clean oil, if given sufficient time. Since, in general, stresses occurring across small portions of a winding are much less during full wave application than for a chopped wave, a fault may not be detected, unless the full wave is immediately applied. This is one reason why failure detection during steep-front test is difficult because usually the impulse circuit must be changed which can take considerable time. At times this may be sufficiently long to seal off the fault with clean oil. I would suspect that this occurred in the case of Figure 23 of the paper.

4. Our experience shows that: if a 100 per cent voltage wave is applied rapidly (within a minute) after a chopped wave, it will indicate a previously established fault except in high-impedance windings. In all cases where the voltage wave will indicate a fault, the current wave gives a much greater indication, and is therefore, more sensitive. There were two cases, where a fault was indicated only by the current wave.

In our opinion, it is therefore essential that current waves be taken during impulse tests on transformers using the necessary precautions as outlined in the revised impulse test code of ASA Standard C57.2.

5. The short circuit of secondary or tertiary windings is attractive from a time-saving point of view. However, for the sake of fault detection, it is not advisable. By short-circuiting a whole winding, a short circuit comprising a fraction of a per cent of the impulsed winding, which constitutes a relatively high impedance, might not be recognized. The use of linear resistors to control the voltage on terminals not tested directly, as outlined in ASA Standard C57, 22.117 is therefore preferable.

W. L. Teague (Westinghouse Electric Corporation, Sharon, Pa.): Although great improvements have been made in the methods of impulse testing transformers, all who test in large numbers will agree that there is still a need for improved methods of detecting faults, and for simplifying and speeding up the tests. Aicher's experiences in detecting failures during impulse testing transformers are most interesting. He has presented some valuable information; but in our opinion has found more complication and confusion in excitation, and more virtue in neutral-current measurement as a means of detecting failures than is justified. Our experiences, based on association with the Westinghouse Laboratories since the beginning of impulse testing, leads to a different evaluation of excitation and neutral-current measurement.

When impulse tests were first standardized for transformers the aim was to reproduce in the laboratory, as near as possible, the same impulse-voltage conditions that exist in service. The transformer was to be excited at normal voltage and the impulse wave was to be applied on a crest of the excitation voltage of opposite polarity. There were two reasons for specifying excitation.

The first reason was to produce voltage-stress conditions in the winding similar to those in service. With the impulse synchronized to strike at the crest of the excitation wave of opposite polarity, the greatest change in voltage at the tested end of the winding will be experienced. The resulting voltage stress conditions in the winding will represent the worst that can be experienced in service. This contribution of excitation to the voltage stress conditions in the winding is a virtue of excitation that has not been mentioned in the paper. If a transformer does not have uniform voltage distribution the omission of excitation requires a sacrifice in stress between the turns and coils at the line end of the winding.

The second reason for excitation was a means of fault detection by magnetic oscillograms of the excitation current and voltage at the instant the surge strikes. In service the first evidence of a transformer failure from impulse voltages is power follow. If power did not follow impulse failures in service it would be a rather happy condition. We would have in our transformers the equivalent of automatic built-in lightning arresters. When impulse tests were first standardized it was reasoned that power would follow impulse failures in the laboratory the same as it does in service. This reasoning is sound provided all conditions are reproduced, but it is difficult to supply excitation circuits with recovery rates as in service. In our laboratory experience we have found that power usually follows impulse failures, but not always. When power does follow the magnetic oscillograms will show evidences of trouble. In a few cases, in our experience, the magnetic oscillograms were the only indication of failure. Usually, however, other evidences of faults are also present. In addition, where power follow is involved, excitation will aid in detecting faults by increasing the amount of smoke, bubbles, and noise.

Another advantage of excitation is that it aids in the location of the place of failure in the winding. In cases where power follows, even though it be very slight, it leaves a mark that is easier to locate on inspection than faults from impulse tests without excitation.

In evaluating excitation then it is a problem of balancing the advantages of the extra voltage stress conditions in the winding, and the aid in fault detection and fault location against the disadvantages of the extra complications, and the extra time required in making the tests. Our experience, based on a large number of tests with excitation, would lead us to conclude that excitation has served its purpose well. For development and verification tests we have found the advantages will justify the extra trouble and time. Since the ASA Standards now make excitation optional in commercial impulse testing, and since impulse testing has proved so valuable in detecting weaknesses and faults in windings; we have sacrificed the advantages of excitation on quality control test in order to speed up and streamline the tests so that more units can be tested.

Neutral current measurement as a method of detection consists of measuring the impulse current for a reduced full wave, and comparing this pattern current wave with that for the full-wave tests. If these two current waves are identical in form it is assumed that no failures have occurred.

Neutral current detection is applicable to full wave tests only. Current measurements have no value for any kind of chopped-wave tests. For detecting failures that may occur on front-of-wave or chopped-wave tests by this method, it is necessary to follow such tests by a full wave and to assume that any failures will reappear on the full-wave test and show irregularities in the current oscillogram. In our opinion this is not a reasonable assumption to make, since National Electrical Manufacturers Association (NEMA) steep-front waves are 30 to 70 per cent greater in magnitude and ASA chopped waves are 15 per cent greater than the full waves. Besides the two types of tests are intended primarily to stress different parts of the winding. Even though the full-wave tests are applied soon after the front-of-wave or chopped-wave tests, there is no assurance that the failure will reappear on the full-wave tests and be detected by the current oscillograms. This is a great weakness in the neutral-current method of detection.

Another limitation in the neutral-current method of detection is in the case of failures that may occur across a small percentage of the winding near the line end. In these cases the stresses result from the initial or capacitance voltage distribution, and remain across the part of the winding an exceedingly short time. Since the initial rush of current through the capacitance of the winding is high in magnitude, and usually oscillates violently, the first ten or more microseconds of the current wave may be so irregular as to make detection difficult and uncertain.

Our experience with the current method of detection leads to the conclusion that it is not a super-sensitive or a sure-shot positive method. It is just another method that also has weaknesses and limitations. Current measurement adds complications and requires more time in testing, since repeated tests must be made if one oscillograph is used. A better alternative would be to use an extra oscillograph or a 2-element oscillograph so that the current and the voltage oscillograms could be made simultaneously, but even this complicates the tests and requires extra time. In evaluating current measurement as a means of detecting faults we have not been able to justify its regular use in commercial impulse testing. In some types of failure, such as those in the interior or near the grounded end of the winding resulting from oscillations, it is a useful method. We use it when needed for investigating suspicious or questionable cases along with any or all other methods of detection.

Impulse testing has contributed so much to the improvement of the dielectric strength of transformers that every effort should be made to simplify and streamline the tests so that more and more units may be tested. Sacrifices to simplify and speed up the tests may well be justified. However, any additions tending toward complicating or increasing the time of impulse testing should be evaluated carefully.

H. H. Wagner (Pennsylvania Transformer Company, Canonsburg, Pa.): The author has presented some interesting data with rather significant implications and conclusions. Our own experience in commercial and laboratory impulse testing since 1932 confirms most of the conclusions in the paper with one or two exceptions.

We have found that normal frequency excitation during impulse tests serves no useful purpose, as power current follow rarely occurs, and also makes the detection of failures very difficult. In no case has a failure been detected by means of "power follow" using 60-cycle excitation.

Our use of neutral current measurement for over four years indicates that it is a very sensitive and fairly reliable method of failure detection. In fact, we have for the past $2^1/_2$ years taken neutral current oscillograms on all transformers which were given impulse tests.

With reference to Figures 12 and 23 in the paper, I believe it is doubtful whether

a failure is indicated conclusively in the oscillogram of a steep-front wave itself, without substantiation by subsequent full-wave tests. I do agree, however that it may be possible to have a failure during the application of the steep-front wave, and yet have no failure during the subsequent application of a $1^1/_2$ by 40 full wave. Although no AIEE or ASA standards have been established for steep-front wave testing, some commercial steep-front tests are occasionally being made which are probably not properly co-ordinated with the $1^1/_2$ by 40 wave levels.

L. C. Aicher: The variety of thought on this subject is very interesting. I am sure much good has been accomplished toward developing a better understanding of failure detection during impulse tests of transformers. It is regretted that some of the critics claim to take issue with statements in the paper, yet fail to make their thoughts known.

The intent in preparing this paper was to present an objective study of impulse failure detection methods. Individuals whose business involves witnessing impulse tests in the factory have expressed the need for such a study. They were particularly interested in a comparison of the relatively new neutral current method and the test with normal excitation. Certain other methods were not included because they are adequately discussed elsewhere.

Figure 23 of the paper has provoked considerable discussion. There is some feeling that because the neutral current method did not detect failure, in this instance, the method is of questionable value. Neither did the ASA Standard chopped waves or full wave detect the fault. As all who have had actual experience with impulse testing know, the phenomena displayed in Figure 23 is not uncommon. In a number of instances, steep wave tests have produced failures which have "healed" presumably by replacing old oil with new and then passed the recognized ASA test without incident. It is felt this criticism of the neutral current method is unjust because how can a method detect a failure when no failure occurred in the shots in question?

Hagenguth has very clearly summarized the outstanding characteristics of the neutral current method of failure detection.

Rohlfs indicates that a mismatch of the oscillations following "chop-off" on the steep-front waves may or may not indicate failure. As he points out one must be cautious in interpreting these discrepancies. His analysis of the many factors which can influence this portion of the oscillogram is correct. However, by proper arrangement of the high-voltage generator and meticulous care in the layout of the grounding network, much can be done to confine discrepancies to the transformer circuit. It is very important to compare steep wave oscillograms made under identical conditions. So little as lengthening the gap a fraction of an inch can produce discrepancies bearing no relation to the transformer. Our confidence in this method of detection has been bolstered by two experiences in which no other indication of fault existed. In each case the diagnosis proved to be correct.

Rohlfs and Hagenguth have raised a question regarding the wisdom of short circuiting those windings not being tested. This question has appeared before. An investigation was conducted using both the short circuited connection and an open circuit of the winding not under test. Aside from a noticeable change in the slope of the full wave the results were strikingly similar. The neutral current measurement possessed different frequency components but the existence of a known fault was equally noticeable in either. Subsequent experience has produced the interesting fact that the indication on the neutral current oscillogram can be greater in some cases, with the nonsurged windings short circuited.

A second criticism of this method of grounding the untested winding concerns the effect it may have on the voltage between the tested winding and idle winding. The claim has been made that such a connection decreases the voltage between these windings. The fact is it may also increase the voltage across the major insulation between the two windings. It is a matter of the polarity of the transformer, the arrangement of the windings and how the transformer is grounded for test.

Teague, Vogel and Mulcahy are unduly disturbed for fear the elimination of excitation during impulse tests will reduce the voltage stress conditions in the winding to undesirable levels. This reasoning seems to have entered the picture in recent years for it does not appear in the early writings regarding the reason for and use of excitation during impulse test. The early writings indicate that the principal benefit of excitation was for failure detection. An analytical approach to the joint use of excitation with impulse test indicates that under certain conditions an increase in stress could be expected. Efforts to confirm this solution have been most confusing. Some data indicates a slight increase, much with no change, some a decrease and still another set of data we have seen indicates an appreciable increase.

Before we become too distributed over the voltage stress question, let it be remembered that the steep-front wave test, to which Teague once referred, is from 30 to 70 per cent greater in magnitude than the ASA full waves, whereas, the change in stress which Vogel calculates is of the order of 14 per cent. As long as the steep-front test is used what is anyone worrying about? This test is already at a much higher level and produces stresses much greater than apparently can be produced if we accept that excitation does increase stresses.

Teague seems to feel that neutral current measurements have no value with any kind of chopped wave test. Certainly for chopped wave tests the characteristic of a neutral current measurement up to the time of chopping should be identical, except for magnitude, with that obtained on the full wave and reduced voltage wave. Since the chopped wave is at approximately 15 per cent higher voltage than the full wave, there is a good possibility that it will detect any faults which occur due to this higher voltage, up to the instant of chopping. This is demonstrated in Figure 16 where injurious corona prevailed just before the sample failed. As for indications after the wave has chopped experience is too limited to make conclusions. In general the same comment applies to steep-front tests where the neutral current has been measured. Experience indicates that with proper choice of oscillograph sweep (Figure 16) the early oscillations of the current neutral measurement do duplicate as long as no faults occur and conversely do not match when there are indications of faults.

It is difficult to agree with Mulcahy's conclusion that to eliminate excitation will permit a reduction in the insulation margin. If the analytical solution is accepted the greatest increase in stress would be near the surged terminal. As long as transformers are designed to meet the existing steep-front levels as well as the ASA impulse levels it is not possible to lower the insulation below present design practice. In many cases the design of the transformer is such that even the extreme stress indicated by the analytical approach is smaller than the margin necessary to cover manufacturing and material variation. Therefore, we cannot agree that the elimination of excitation is a step in the direction of lowering the standards.

There seems to be general agreement among all the critics of the desire to simplify the impulse test procedure. Some are willing to sacrifice what they believe to be a necessary test, if such a sacrifice furthers the cause without jeopardizing the product's reliability. With this in mind, the writer suggests that thought be directed toward substituting the NEMA front-of-wave impulse test for the present ASA chopped-wave test and follow with the present ASA full-wave test. Associated with this suggestion is one for a study of the possibility of adjusting the impulse generator so that it will be only necessary to change the charging voltage in order to change from a steep-front wave to a full-wave test. This may necessitate some modification in the tolerances or shape of the front of the full-wave test. If such a procedure can be followed it will permit considerable saving of time. There are very few transformers where the chopped-wave test dictates the design in preference to the steep-front test.

The writer is perfectly in accord with the practice of making standards cover general practices as far as test methods are concerned. The standards should cover all applicable methods. The writer would like to recommend, however, that the standards permit the group doing the testing to select which method it desires to use. Each testing facility has its own characteristic traits known best to the operators of that facility. Therefore, each testing group should be allowed to use the method that gives them the most satisfactory results.

Bellaschi's comments regarding European practice are interesting although somewhat at variance with the views of European engineers expressed in their technical journals.

It would be interesting to have additional thoughts on this subject from the users of the equipment.

Voltage Stresses in Distribution Transformers Due to Lightning Currents in Low-Voltage Circuits

K. D. BEARDSLEY
MEMBER AIEE

W. A. McMORRIS
MEMBER AIEE

H. C. STEWART
MEMBER AIEE

INFREQUENT reports of severe mechanical damage to small transformers as a result of nearby lightning strokes have led to the conclusion that the cause of the damage was the passage of heavy lightning currents through the two halves of the 3-wire low-voltage winding from the neutral to outgoing lines or the reverse. These cases have been rare, but are of considerable interest as they have led to the consideration of the effects of the more frequent lightning currents of smaller magnitude flowing through the same paths.

Considerable laboratory work done over a period of several years, supported by field investigations and service experience, has shown that relatively small currents entering the transformers by way of the low-voltage secondary service leads are the probable cause of an appreciable number of transformer lightning failures. They can result in the failure of insulation in either the primary winding or secondary winding, or both. The most probable point of failure is in the high-voltage primary winding. Lightning arresters connected to the high-voltage winding, although effective for surges coming from the high-voltage lines, are ineffective in controlling this type of damage because it can take place even with both high-voltage terminals solidly grounded.

Laboratory Tests

Studies have been made of the voltage stresses in distribution transformers when the low-voltage windings are subjected to impulses of varying lengths. Several connections of the high-voltage and low-voltage windings have been investigated. A comprehensive study also has been made of the effects of various physical arrangements of the windings. The most important result of this work is the discovery that destructive voltages may appear within a high-voltage winding due to lightning currents in the low-voltage windings, even though its terminals are both solidly grounded.

Four basic connections of the windings have been used in the laboratory study as illustrated in Figure 1. In the connection of Figure 1A, the impulse currents flow in the same direction in the two halves of the low voltage windings, and one high-voltage terminal is isolated. Conditions are the same in Figure 1B except that both high-voltage terminals are grounded. In Figure 1C, impulse currents flow in opposition in the two halves of the low-voltage winding, and one high-voltage terminal is isolated. The arrangement in Figure 1D is the same as for Figure 1C except that both high-voltage terminals are grounded. It appears that there are conditions under which damaging voltage stresses can be encountered with any one of these four circuits.

A previous paper[1] states that when an impulse voltage is applied to the high-voltage winding of a power transformer, the resulting voltages within the windings may be considered to consist of four components:

1. A voltage transmitted electrostatically to the low-voltage winding, depending upon the relation of distributed capacitances of the low-voltage winding to the high-voltage winding and ground and independent of the turn ratio.

2. A free oscillation in the high-voltage winding, consisting of a number of space harmonics, and which through its electromagnetic and electrostatic fields induces a corresponding oscillation in the low-voltage winding.

3. A free oscillation in the low-voltage winding, depending upon the distributed constants of the winding.

4. A unidirectional voltage surge transmitted to the low-voltage winding by electromagnetic induction, directly proportional to the turn ratio and independent of the distributed capacitance.

The same principles apply in the case of Figure 1A. Since the transformation is step-up instead of step-down, the four components described in the previous paper[1] have different relative importance. The electrostatic component which may put serious stresses on the low-voltage insulation of a high-voltage transformer becomes insignificant when the transfer is from low-voltage to high-voltage winding. Due to the few turns and small physical size of distribution-transformer low-voltage windings, the component which has to do with oscillations in the exciting winding also is of negligible importance. The two remaining components therefore are the ones to which attention should be directed. Natural oscillations of the high-voltage winding may be of importance, particularly as they affect internal stresses between layers or sections of high-voltage windings. It is well known that the electromagnetic component is sloped off by the effect of the leakage inductance between transformer coils. For this reason, for impulses of very short duration, the electromagnetic transfer may not result in large voltages. However, for impulses of greater duration, the voltage appearing at the isolated high-voltage terminal will approach that specified by turn ratio. In small distribution transformers, the turn-

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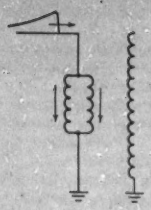
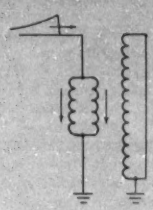
K. D. Beardsley, W. A. McMorris and H. C. Stewart are all with the General Electric Company, Pittsfield, Mass.



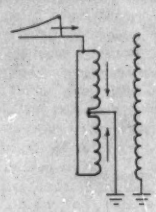
A

Impulse currents in two halves of low-voltage winding in the same direction, one high-voltage terminal isolated



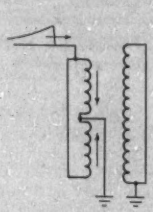
B

Impulse currents in two halves of low-voltage winding in the same direction, both high-voltage terminals grounded



C

Impulse currents in two halves of low-voltage winding in opposite directions, one high-voltage terminal isolated



D

Impulse currents in two halves of low-voltage winding in opposite directions, both high-voltage terminals grounded

Figure 1. Transformer connections for laboratory tests of impulse excitation of low voltage windings

ratio normally ranges between 20 to 1 and 110 to 1.

In Figure 1B the high-voltage winding is short circuited. In this case, the electromagnetic component cannot appear at the terminals, but tends to force oscillations within the winding.

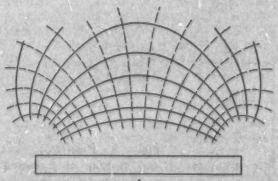
In Figure 1C, the opposing impulse currents in the two halves of the low-voltage winding might be expected to cancel out so that there would be no net electromagnetic effect. In an actual transformer, however, individual turns of two windings cannot occupy identical locations, so that perfect current balance and flux cancellation do not exist. In fact, minor manufacturing variations in the positioning of low-voltage turns make surprising differences in the results obtained. One way of visualizing the phenomenon is to replace the low-voltage winding by a fictitious exciting winding with turns corresponding to the resultant unbalanced ampere-turns of the opposing coils. The very large turn ratio between the high-voltage winding and this fictitious winding explains the surprisingly large voltage which may appear at the high-voltage terminal.

When the terminals of the high-voltage winding are connected together, as in Figure 1D, oscillations are forced within it. In some cases the excitation not only may be large, but also of an asymmetrical pattern, so that high stresses will result in the high-voltage winding, not only between layers or sections, but also between portions of the winding and ground. Damaging stresses of this type are more apt to occur in transformers of the higher voltage ratings. This is due to the greater turn ratio and to the increased susceptibility toward internal oscillations of coils with many turns.

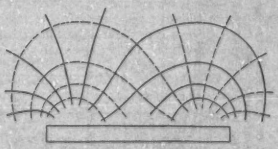
In order to understand the relationship between coil design and the type of stresses to be encountered with the various laboratory connections illustrated, many kinds of low-voltage and high-voltage winding construction have been studied. Investigations of flux fields were made by means of a search coil and vacuum tube voltmeter. Opposing current flux plots for two typical simple low-voltage coil arrangements are shown in Figure 2. Variations and combinations of these two types of flux plots were obtained for the different coil designs investigated. It is generally possible to predict the performance of a given coil arrangement by determining its flux pattern, resolving it into radial and axial components, and then estimating the performance under the various possible circuit conditions.

In order to set up test conditions in the laboratory which would simulate the most severe field conditions, a special low-voltage high-current impulse generator was built. By winding high-voltage coils with numerous taps, it was possible to measure with a cathode-ray oscillograph the voltage stresses between taps and from taps to ground. Figure 3 shows envelopes of oscillations measured for two typical distribution-transformer coil arrangements, tested with opposing low-voltage impulse currents and short-circuited high-voltage windings.

The oscillations encountered in a short-circuited high-voltage winding often are a combination of the two types shown in Figure 3. The second space harmonic, predominant in Figure 3A, is apparently a phenomenon related to natural internal frequencies dependent upon the physical dimensions of turns and layers. Tests show that this oscillation is affected very little by the energy in the applied impulse and the presence or absence of an iron core. The fundamental oscillation, predominant in Figure 3B, is reinforced by the presence of the transformer core. A copper shield between high-voltage and low-voltage windings, by means of eddy currents within the shield, tends to control the flux linking the high-voltage winding. As the shield thickness is increased, its effectiveness in cutting down the

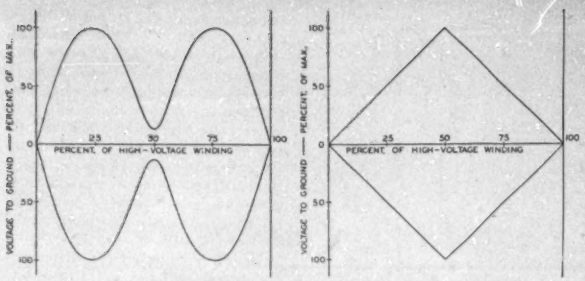


A



B

Figure 2. Flux plots with currents flowing in opposite directions in the two halves of two typical low-voltage windings



Second space harmonic predominant — Fundamental predominant

Figure 3. Typical envelopes of oscillations in high-voltage windings (with both terminals solidly grounded) caused by impulse excitation of low-voltage windings

fundamental oscillation is, therefore, also increased.

In laboratory tests on typical distribution transformers, the response to applied impulses of short duration often is as shown in Figure 3A. As the length of the applied impulse is increased, the fundamental becomes more and more important, with little change in the second harmonic, so that the envelope of oscillations may finally approximate that shown in Figure 3B. For some types of high-voltage construction, the envelope approximates a sinusoidal rather than an angular envelope such as is shown in Figure 3B.

Considerable control of this type of voltage stress is possible by designing the coil arrangements with this phenomenon in mind. Practical designs have been evolved which reduce the magnitude of the fundamental oscillation to a low value. In general, however, there is a basic difficulty in designing transformer windings which are completely satisfactory for all possible conditions. For example, it frequently has been found in laboratory testing that a transformer which has relatively low stresses when connected as Figure 1D, may be severely stressed when connected as Figure 1C. In the field when opposing impulse currents flow through the windings, there is no assurance that they will be exactly equal in magnitude. Very small variations in winding or in coil alignment can result in abnormal performance. Extreme design measures which might be taken to minimize these effects under some conditions would be ineffective under other conditions, as well as economically impractical.

Field Studies

Several significant results have been obtained in the studies of this phenomenon that have been made in the field.

Tests have been made on a Y-connected rural distribution system to determine the relative number and severity of lightning surges on the secondary conductors. This was done by the use of surge crest ammeter links[2] which were installed on every conductor emanating from the transformer locations under study. The results as shown in Figure 4 indicate that the number and severity of the surge currents arriving at the transformers by way of the secondary leads or pole grounds were nearly as great as for those arriving by way of the primary leads. Figures 5 and 6 illustrate the current distribution in typical cases of surges arriving over secondary leads or pole grounds. Values entered opposite solid arrows are measured values of surge currents in crest amperes. Those entered opposite dotted arrows are estimated. The direction of the arrows indicates the direction of propagation of the surge and not its polarity.

It was recognized from the results of the laboratory studies that lightning surges entering the transformer by way of either the primary or secondary terminals could result in failure of primary insulation with no other damage to the transformer. Therefore, statistical studies of primary versus secondary insulation failures would not give a reliable indication of the relative severity of primary and secondary lightning surges.

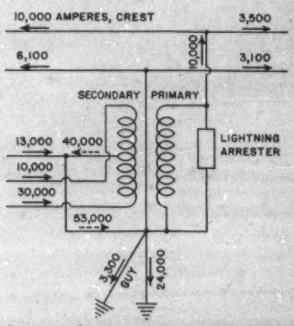


Figure 5. Distribution of surge currents at a distribution transformer location

Surge arrived by way of secondary service conductors

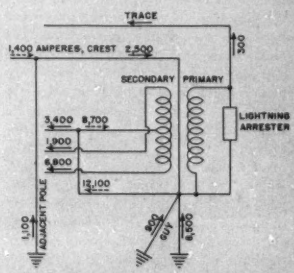


Figure 6. Distribution of surge currents at a distribution transformer location

Surge arrived by way of pole grounds

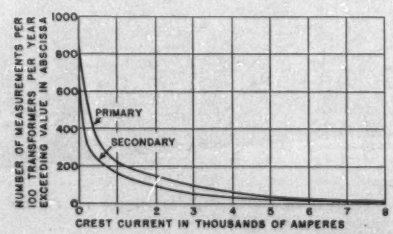


Figure 4. Relative number and severity of measured surge currents on primary and secondary conductors at distribution transformer locations

It was evident that the best lightning protection to the transformer would be afforded by applying lightning arresters to the secondary as well as to the primary terminals. To evaluate the benefit derived from the secondary lightning arresters, lightning failure data were kept over a period of three years on two groups of distribution-transformer installations. Both groups were located on the same distribution system and, as far as could be determined, had the same degree of lightning exposure. The transformers and primary lightning arresters were the same in both groups. One group of transformers was equipped with secondary lightning arresters, and the other was not.

The type of lightning arresters used on the secondaries is shown in Figure 7. These lightning arresters are small and inexpensive. They are capable of discharging a 5 by 10 microsecond impulse current of 65,000 amperes. Their impulse sparkover is only 2 kv at 10 kv per microsecond, and their discharge voltage is only 2.5 kv while discharging a 10,000-ampere 10 by 20 microsecond impulse current. Two poles, each rated 175 volts alternating current, are housed in a cylindrical aluminum container only $2^3/_8$ inches in diameter by $2^1/_4$ inches long.

For each of the three years, the failure rate was consistently much lower for the group of transformers with secondary lightning arresters. Considering the entire period, the transformers with secondary lightning arresters had a failure rate of less than one-quarter that of the other group.

It should be appreciated that these results were obtained in a rather limited investigation involving only one distribution system. However, these results, together with the results of the laboratory studies, demonstrate conclusively that under certain operating conditions an appreciable number of lightning failures of distribution transformers can result from secondary impulse excitation. This phenomenon can be minimized to some extent by the design and arrangement of the transformer windings, but all winding arrangements commonly used are subject to some degree to this type of trouble. The most efficient distribution lightning arresters applied to the primary terminals are generally ineffective in preventing this type of damage, even to the insulation of the primary winding. The application of lightning arresters to both the primary and secondary terminals provides the most complete solution and can be justified economically if the lightning exposure of secondary circuits is severe or if a greater continuity of service is desired.

Conclusions

1. Laboratory tests show that lightning currents entering distribution transformers through the low-voltage secondary terminals can cause damaging voltages in the primary windings.

2. Lightning arresters connected to the primary terminals are generally ineffective in protecting the primary windings against voltages produced by lightning currents entering the transformer by way of the low-voltage secondary terminals.

3. A field study on a rural distribution system showed that surge currents arriving at distribution transformers over secondary leads or pole ground leads were nearly of the same frequency and severity as those arriving over primary leads.

4. Suitable lightning arresters are available for connection to the secondary terminals of distribution transformers for protection against secondary surges.

5. Comparative field tests on transformers with and without these secondary lightning arresters, in addition to the usual primary lightning arresters, showed that the secondary lightning arresters reduced the transformer failure rate to less than one-quarter the rate without secondary lightning arresters.

6. Installation of secondary lightning arresters at distribution-transformer locations is economical where secondary exposure is high or where a greater degree of continuity of service is desired.

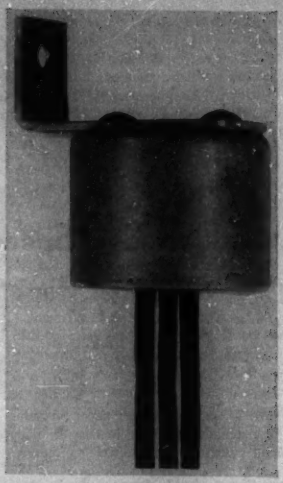


Figure 7. General Electric 2-pole 175-volt pellet service protector for bracket mounting

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1. Effect of Transient Voltages on Power Transformer Design-IV, **K. K. Palueu, J. H. Hagenguth.** AIEE Transactions, volume 51, September 1932, pages 601–20.

2. Direct Measurement of Surge Currents, **C. M. Foust, J. T. Henderson.** Electrical Engineering (AIEE Transactions) volume 54, April 1935, pages 373–78.

Discussion

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The condition of surge voltages being transmitted to the high-voltage side of distribution transformers of sufficient value to cause failure of the high-voltage winding is particularly aggravated because of the disproportional insulation level of the two windings. In a transformer rated 7,200 volt, 120/240, the turn ratio is 60/30:1, while the insulation ratio from basic insulation impulse levels for the two voltage ratings is 75/30 or 2.5:1. It is, therefore, not necessary to transfer voltages from secondary to primary at a rate much in excess of 2.5:1 in order to exceed the insulation strength of the primary winding as compared with the theoretical possible transfer of 30:1 and more.

If secondary lightning arresters are used, not only is the voltage applied across the secondary terminals reduced, but in most cases the peak currents are shunted through the lightning arrester, by-passing the secondary winding. This also helps greatly to prevent the phenomenon of mechanical distortion of secondary windings.

As pointed out in the discussion of a previous paper[1], low ground resistances should materially assist in preventing current flow through the secondary windings by confining the lightning current to the point where a stroke occurs, either at the house or at the pole. While low ground resistance frequently is expensive to procure, a grounded and buried bare neutral conductor in the form of a counterpoise between the house and the transformer should assist materially in distributing lightning discharges directly into the ground rather than through the secondary winding, in those cases where it is difficult to get a reasonably low ground resistance by other means.

Reference

1. Abnormal Currents in Distribution Transformers Due to Lightning, **J. M. Bryant, M. Newman.** Electrical Engineering (AIEE Transactions) volume 61, August 1942, pages 584–68. Discussion, page 1005.

Electronic Speed Regulators for Sectional Paper Machine Drive

V. B. BAKER
ASSOCIATE AIEE

J. F. KOVALSKY
NONMEMBER AIEE

THE DRIVE for a paper forming machine is unique in that it must meet extremely rigid operating requirements not generally encountered on any other industrial machine. Control and regulation of the machine as a unit and of its components must be provided to a degree of refinement uncommon on machines of such huge proportions. The type and character of the loads vary widely. Continuity of service on a 24-hour a day 7-day a week basis is imperative.

Operation of the entire process from preparation of the raw material to finishing and shipping room activities are contingent upon uninterrupted production of paper from the paper forming machine. All processes leading up to the machine have multiple facilities, whereas the paper machine is a bottleneck and sets the pace for the entire mill. When the paper forming machine is down, it is not long before the whole mill is down. Hence, in addition to the requirements for the highest quality of performance, the drive equipment must also possess a reliability far greater than is required of the average electric drive.

Control for modern paper machine drives include industrial-type electronic devices that not only meet the stringent operating requirements, but also provide several important advantages over the control elements which they replace. Instantaneous response is inherent and extremely high amplification, where required, can be obtained without delicate mechanical adjustment. There are no moving or wearing parts. Initial adjustments are not subject to change over long operating periods. Optimum performance can be obtained with relatively low maintenance. These features are all an integral part of a new all-electronic speed regulator designed exclusively for paper machine drives.

A paper forming machine is essentially a combination of a number of machines each performing a special function in the complex process of forming a sheet of paper. On one type of machine, the sheet is initially formed on the Fourdrinier wire or couch section from stock consisting of $99^1/_2$ per cent water and 1/2 per cent cellulose fibres (pulp). When the sheet leaves the couch roll, sufficient water has been removed so that the sheet is self-supporting. It then passes through two or more presses where more water is removed. Next the dryers receive the sheet at approximately 60 per cent water and 40 per cent pulp by weight and remove all but 6 to 8 per cent of the moisture. The calenders then apply a finish to the dry sheet in a manner similar to ironing. The finished sheet is then wound into rolls on the reel section. In addition to these main sections, the paper machine may also include other special sections such as smoothing press, size press, or breaker stack as required for the production of different types of paper. A cylinder-type machine is similar to the Fourdrinier-type except for the forming end. On this type the sheet is formed on cylinder molds and carried through various auxiliary presses to the first main press on an endless wool felt. On machines designed for the production of creped tissues and toweling, all wet end sections including a large dryer roll known as a Yankee dryer are mechanically tied together and operate as a group. The dry end of the machine is operated at reduced speeds to provide the desired amount of crepe.

The operation of the various sections must be co-ordinated and their speeds synchronized to form a single continuous process However, the speeds of adjacent sections cannot bear a fixed relation but must be adjustable to meet the draw requirements for different types and grades of paper. Draw is the small difference in linear speed between adjacent sections usually resulting in a slight stretching of the sheet. Moreover, section speeds, when once set for a specific production run, must be held constant regardless of load changes or other system disturbances

Figure 1. Press section drives on a Fourdrinier machine (Alabama Pulp and Paper Company)

Note the small a-c speed indicating tachometer on the left side of the drive assembly direct coupled to a high-speed shaft extension of the speed reducer

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V. B. BAKER is with the Westinghouse Electric Corporation, East Pittsburgh, Pa., and J. F. KOVALSKY is with the Westinghouse Electric Corporation, Buffalo, N. Y.

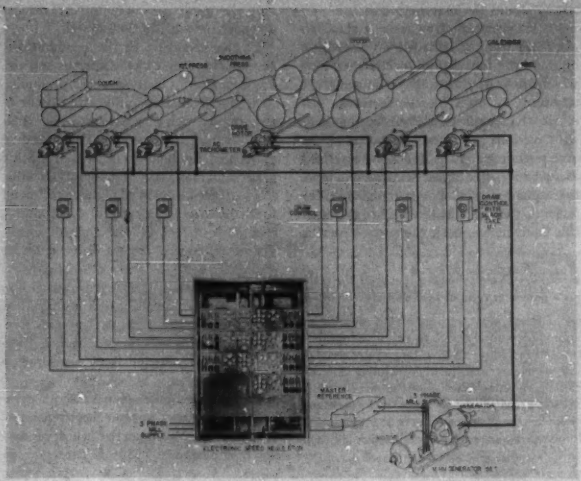


Figure 2. Diagrammatic representation of the single generator type of sectional electric drive

Each of the all-electronic regulators in the cubicle at bottom center supply the excitation requirements of the individual section drive motors. A statically regulated master reference serves as a bench-mark to which section speeds are matched

in order to maintain uniform draws. Constant speed on each section of the machine under all conditions is essential to insure a high quality of product which can result only if the paper machine drive provides a uniform operating speed of the machine as a whole and accurately maintained draws between sections.

The two principal types of drives for paper machines are the mechanical or lineshaft drive and the sectional electric drive. The latter type consists of individual drive motors applied to each section of the machine requiring independent draw control. All sections of the machine must operate as independent units; the only mechanical link between sections is the sheet itself. Figure 1 shows a complete section drive unit including d-c motor and speed reducer both mounted on a common bedplate. Modern high-speed machines may have a total of more than 2,000 horsepower of applied motors with some individual sections having as much as a single 500-horsepower drive. Sectional electric drive has been applied to a large number of paper machines of all types. It provides improved operating practices, low maintenance, accurately maintained draws, requires a minimum of space about the machine, and permits the application of almost unlimited horsepowers per section without undue complication.

Present forms of sectional electric drive include the single generator system diagrammatically represented in Figure 2 and the multiple generator system as shown in Figure 3. In the single generator system, one generator supplies power to all of the section drive motors. A separate momentarily rated generator is included for starting and accelerating service, and for inching the various sections as required during operations such as felt or wire replacement and wash-up. The operating speed of the whole machine is controlled by adjusting the main generator voltage, and individual section speed is regulated by controlling the excitation of each section motor shunt field.

The multiple-generator system includes individual generators for each section motor and suitable control for each generator. Over-all machine speed is adjusted by changing a master reference voltage which is part of the speed regulating system. Section speed is regulated by controlling the excitation of the individual generators.

Some idea of the problems associated with the application of speed regulators to sectional paper machine drives is indicated by a review of a few of the more important operating requirements. Tests show that for some types of paper, section speeds on the dry end of the machine where the draws are most critical must be held within ± 0.05 per cent during normal operation. This accuracy is well within the limits wherein the sheet will neither sag down nor break. During transient disturbances, larger speed variations can be tolerated provided their durations are limited.

Moreover, the extreme accuracies required of the speed regulating equipment must be obtained even though the section load requirements and operating practices impose rather severe duty on the drive. Variations in vacuum conditions on the couch sections may cause swinging if the regulator does not possess the proper antihunting features. Changes in nip pressures at the press sections and doctor blade pressure on dryers and

Table I

Section	HP Constant HP per Inches per 100 Feet per Minute	Calculated HP	Applied HP	Estimated Running Load, Per Cent of Rating	Section WR^2, Pounds-Feet**	Stored Energy, HP-Seconds	Acceleration Time, Seconds†	Motor Fld. Time Constant, Seconds	Mech. Time Constant, Seconds
Couch	0.100	306	300	88	720	265	1.8	1.4	0.1
Presses (2)	0.030	91.5	100	69	850	550	11	0.9	0.9
Smoothing press	0.012	36.8	40	69	640	450	22.5	0.6	2.3
First dryer, 26–60"	0.0027*	215	200	72	35,100	8,800	88	1.1	6.0
Second dryer, 26–60"	0.0027*	215	200	72	35,100	8,800	88	1.1	6.0
Breaker stack	0.012	36.8	40	69	640	450	22.5	0.6	2.3
Third dryer, 26–60"	0.0027*	215	200	72	35,100	8,800	88	1.1	6.0
Size press	0.012	36.8	40	69	640	450	22.5	0.6	2.3
Fourth dryer, 20–60"	0.0027*	165	200	55	27,200	6,800	68	1.1	4.8
Calenders (2)	0.100	306	300	88	3,800	1,430	9.5	1.4	0.6
Reel (empty)	0.020	61.3	60	100	160	105	3.5	0.7	0.3

* Per dryer roll

** Referred to the motor shaft

† Assuming 100 per cent torque available for acceleration

calenders produce load variations. Cascading of water in the steam-heated dryer rolls due to improper condensate evacuation causes erratic load swings on these sections. Calender frictions are far from constant. These undesirable conditions must be successfully handled on a machine whose physical makeup is far from ideal from the regulating standpoint. Table I includes data characteristic of a typical high-speed machine and indicates the extent to which horsepower, inertias, and time constants may vary throughout the machine.

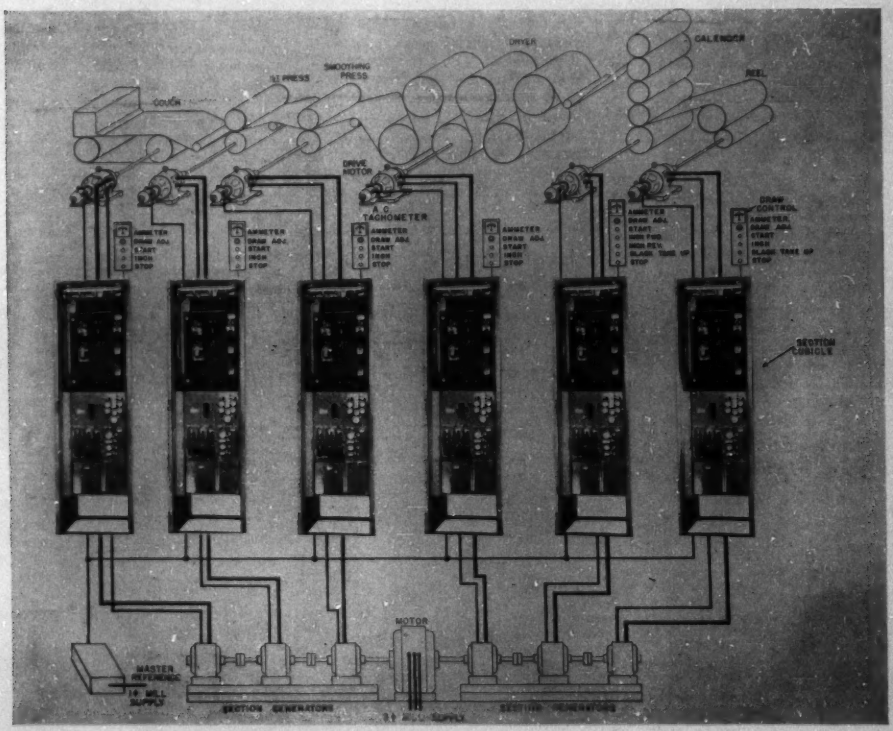
The WR^2 of each section is the total inertia referred to the motor shaft. It will be noted that light inertia sections are located between high inertia sections. In addition, the length of sheet between the smoothing press and the first dryer is usually rather short. Considering the widely different mechanical time constants of these sections, it is apparent that, without modification of some sort, the problem of maintaining uniform draws would be extremely difficult. To ease this situation, flywheels of specified sizes are usually included on the drive equipment for light inertia sections which are located between or immediately adjacent to high inertia sections. In effect, the inertia characteristics of these light sections are made equivalent to the heavy sections. Thus, following a system disturbance, all of these sections will tend to respond in a similar manner. In determining the size of flywheel to produce this equivalence, both the section inertia and the electrical characteristics of the drive motor must be considered.

In the operation of the machine, the accelerating times of the various sections are important. On a machine producing light tissues, breaks quite often occur in the dryer sections necessitating their shutdown for clearing while the preceding portion of the machine is still producing paper. Thus, the maximum possible rates of acceleration and deceleration of the section are necessary in order that a minimum of lost production will result. Therefore, control for the drive must be arranged to utilize the section drives to the fullest extent of their ratings and at the same time impose a minimum of disturbance on the rest of the machine. Present designs of sectional electric drive include preset current-limit acceleration and deceleration.

On machines producing kraft paper, a very "tight draw" or high sheet tension is usually carried between the last dryer and the first calender. This is necessary in

Figure 3. Sketch showing layout of the multiple generator type of sectional electric drive

Each independent section of the machine is equipped with a complete set of drive components including drive motor, power supplying generator and totally enclosed control cubicle housing the all-electronic section speed regulator and the required sequencing relays and contactors. The master reference furnishes the speed cue for all regulated sections



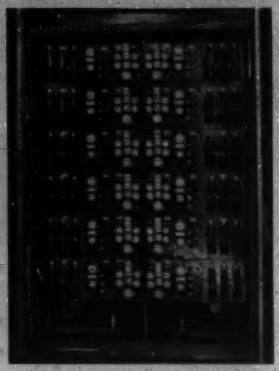


Figure 4. Close-up of a regulator cubicle containing 12 complete regulator assemblies for an 11-section machine

The extra unit serves as a spare and by means of plug-in connections on the rear of the swinging panels, any one of the other units may be replaced for routine maintenance in a matter of minutes. Each panel subassembly includes the error detector, voltage amplifier, and grid-controlled thyratron rectifier for motor field excitation

order to equalize the nonuniformities that may occur in sheet formation. In some cases the tension at this point may be so great that the drive on the first calender section actually pulls the last dryer section. On one machine producing heavy kraft board, the first calender drive actually assisted in driving not only the fourth dryer section but also a size press, the third dryer section, a breaker stack and, to some extent, the second dryer section. Thus, the dryer section drive may operate at practically no load and may even regenerate to full capacity of the drive. Under these conditions, if the sheet breaks between the calender and the dryer sections, a large block of load is thrown quite suddenly on the dryer-section drive. The drive must therefore pick up this load rapidly enough to prevent the accumulation of a loop known as sag-back between the last dryer and the preceding dryer or breaker stack. If sufficient loop is allowed to form, a wrinkle may occur as the sheet enters the dryer. Before this wrinkle has proceeded through the dryer, the operators, being unaware of the presence of a wrinkle, may have rethreaded the sheet into the calender. When the wrinkle reaches the calender, a cut will occur which almost invariably causes another break. This recurring sag-back condition can be prevented if the speed regulating system on the last dryer section is extremely fast and has sufficient forcing action to keep the initial sag in the sheet below a permissible maximum following a break.

Moreover, an unusually large loop may cause a wad or wrap in the dryer section necessitating a shut-down for cleaning. The lost production resulting from either a recurring break at the calender, a wad or a wrap in the dryer dictates a regulator design that is capable of responding under this severe operating condition. The all-electronic section speed regulator in combination with a booster generator is well able to meet these stringent operating requirements.

From a consideration of the foregoing, it is apparent that the speed regulators form an important part of modern paper machine drives. The new all-electronic regulator for sectional electric drive satisfies all of the widely different and in certain cases extremely difficult application requirements. It is capable of high accuracy, high speed of response, and is reliable and easy to maintain.

Regulation

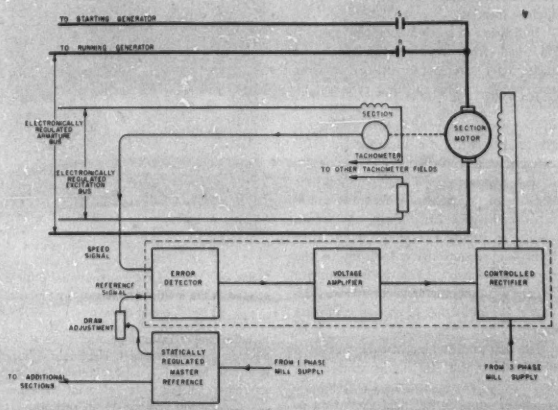
Earlier designs of section regulators were primarily position regulators and employed some form of mechanical speed differential. Due to numerous moving and wearing parts, these drives all required strict adherence to a careful maintenance schedule in order to realize optimum performance. The all-electronic regulator as incorporated in one manufacturer's design eliminates the mechanical differential as well as all other moving and wearing parts of the former designs. Moreover, it provides all of the features of high sensitivity, quick response and effective antihunting essential to the proper operation of sectional paper machine drives.

After considerable analysis and actual field tests, it was determined that a high-accuracy speed regulator could meet the operating requirements. Such a speed regulator permits the use of a simple draw adjusting device consisting of a small potentiometer instead of a mechanical differential and cone pulley assembly as required with a position regulator. Quicker response can be obtained and a speed regulated system generally requires less complicated stabilizing elements.

Use of electronics in a high accuracy speed regulator for sectional electric drive was a natural outgrowth of many years of experience. The merits of electronic regulators for paper mill service have been demonstrated by the excellent service records compiled over long periods of operation as voltage regulators for sectional drive and speed regulators for single motor drives. Extremely quick response, the aim for simplicity of over-all design

Figure 5. Block diagram showing essential components of an individual section drive forming part of the single generator type of sectional electric drive

Current limit control of the starting generator voltage permits acceleration of the section drive smoothly and at the maximum permissible rate. The electronically-regulated running generator and constant voltage exciter busses insure constant over-all machine speed. The electronic regulator in the dotted rectangle maintains constant section motor speed by controlling motor shunt field excitation



and the availability of time-tested electronic components led to the development of an all-electronic system of section speed regulation.

Single Generator System

In this system, Figure 2, a single generator furnishes power to all section driving motor armatures. The regulators for all of the individual sections are located in a common cubicle similar to that shown in Figure 4. The paper machine speed range of say 6-to-1 is obtained by changing the armature voltage on all sections simultaneously by varying the running generator voltage. The speed changes resulting from necessary draw adjustments or variation in section load are obtained by motor field control provided in the electronic speed regulator. The complete section control includes the speed regulator, a speed indicating tachometer, magnetic control, and the operator's control station. Components which are common to all sections include electronic voltage regulators for the running generator and constant voltage exciter, master magnetic control, statically regulated master reference, and speed control for the over-all machine.

The a-c reference voltage regulator is a fully static device, unaffected by changes of voltage or frequency in the mill supply, and provides a master reference voltage to which the voltages proportional to speeds of all individual sections are matched. This master voltage is varied by means of a motor operated rheostat to provide the desired paper machine speed. The operating speed of this rheostat is adjusted to give a rate of acceleration commensurate with the section having the highest inertia.

Indication of section operating speed is obtained from an inductor type a-c tachometer generator directly driven from each section. This tachometer is usually driven from a high-speed shaft extension of the speed reducer as shown in Figure 1. Experience with d-c tachometers indicated that any d-c commutating device in the regulating system is undesirable because of the necessary frequent maintenance, variable brush drop, and the inability to isolate the long conduit runs from the section drive location to the regulator location. For these reasons, a rugged a-c tachometer generator was designed for indication of section operating speed.

Starting and inching of each section is obtained on the starting generator bus and during these operations, the electronic regulator supplies full field excitation to the section motors. The low initial voltage of the starting generator is automatically adjusted to meet the starting torque requirements of the individual section. When starting an individual section, the motor is connected to the starting generator and the excitation of this generator is then increased at a controlled rate to accelerate the section motor at the maximum permissible rate. A Rototrol regulator provides this current-limit accelerating feature. When the starting and the running generator voltages are equal, the electronic speed regulator then takes control, after which the section is transferred to the running generator with a minimum of disturbance to the system.

In the operation of a typical section speed regulator, a signal from the statically regulated master reference and a similar signal from the section tachometer are balanced against each other in an error detecting circuit as shown in Figure 5.

The difference or error voltage is amplified and used to control the 3-phase controlled rectifier which furnishes excitation to the motor shunt field. The circuit is so adjusted that the two signal voltages are balanced when the section is running at its normal operating speed. Under these conditions, the electronic exciter supplies the required excitation to the motor for normal speed.

The control station for each section is located on the tending side of the machine in a position convenient for the operator. It contains "start," "stop," and "inch" pushbuttons and draw adjusting rheostat as well as a load indicating ammeter. On the calender sections, "inch-reverse" and "slack-take-up" are also included.

A master control station provides for preset and manual paper machine speed adjustment, machine speed indication and a master stop pushbutton.

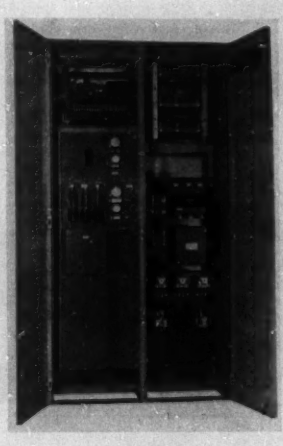


Figure 6. View of the master control cubicle as used in the multiple generator type of sectional drive

Included are a motor operated rheostat for machine speed adjustment, electronic voltage regulator for the constant voltage exciter, statically regulated master reference and other components common to all sections

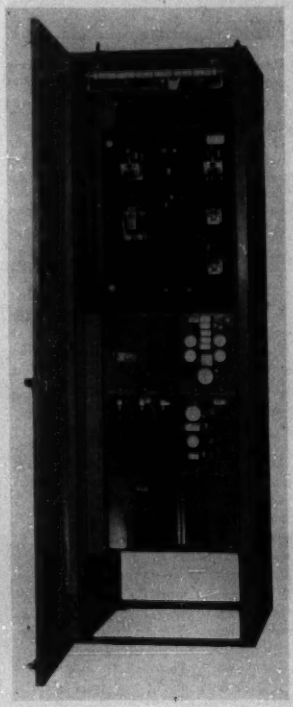


Figure 7. Totally enclosed control cubicle for an individual section of a machine equipped with the multiple generator type of sectional drive

Adjusting resistors and sequencing magnetic control are located at the top above the all-electronic section speed regulator. Ample room and swinging panel design permits ready accessibility and easy maintenance of all equipment

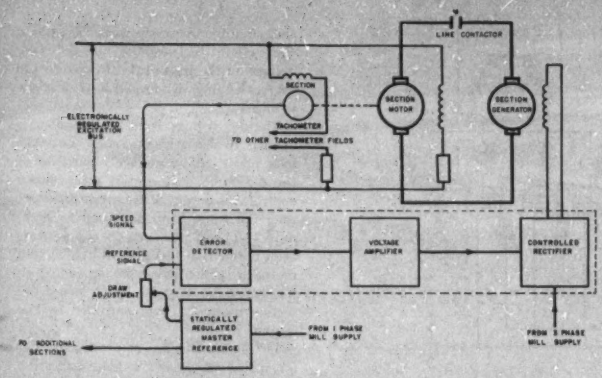


Figure 8. Essential components of an individual section drive as applied to a machine equipped with the multiple generator type of sectional electric drive

Section speed regulation is obtained by control of its generator shunt field excitation. The draw adjusting rheostat alters the magnitude of the reference signal to which the speed signal from section tachometer is matched

Multiple Generator System

In this system, Figure 3, individual generators are used to furnish power to each of the section driving motors. The complete section control includes the speed regulator, a speed indicating tachometer, magnetic control and the operator's control station. A master cubicle (Figure 6) contains an electronic voltage regulator for the constant voltage exciter, control for all motor and a-c tachometer fields, statically regulated master reference source and motor operated rheostat for controlling speed of the over-all machine. The master reference, section a-c tachometer, section control stations, and master control station are the same as those used with the single generator system.

The multiple generator system offers distinct advantages over the single generator system. The applied armature voltage to each section drive motor is correct for all productive speeds of the machine. Because of this fact standard motor frame sizes may be used regardless of the productive speed range. More important from the standpoint of performance, speed regulation of the individual drive by control of applied armature voltage rather than shunt field excitation of the section motors affords greater stability over much wider operating speed ranges. Speed ranges of 10-to-1 are handled easily. Moreover, since shunt field excitation is constant, regulation is obtained at no sacrifice in motor torque per ampere. Thus, better response is obtained with a narrower range regulator than is required with the single generator system. Since each section control is self-contained in its own totally-enclosed cubicle, no interconnecting heavy bus system or elaborate interlocking of sequencing controls common to all sections are required.

With the multiple-generator system, acceleration under current limit control and deceleration with controlled regenerative braking as well as inching both forward and reverse and wash-up speed are all obtained with the electronic control. The only magnetic devices necessary are the overload relay and the main line contactor which serves as a circuit interrupting device for protection of equipment and personnel.

The speed regulating equipment included in the complete section control cubicle shown in Figure 7 is essentially the same as one of the section regulating units contained in the regulator cubicle shown in Figure 4 used with the single generator system. Operation of the electronic regulator used with the multiple generator system is similar to the operation described under the single generator system. However, in this case, the controlled rectifier furnishes excitation to the section generator to produce a motor armature voltage as required for normal speed as shown in Figure 8.

Electronic Circuits

The error detector element of the electronic regulator is shown in block form in Figure 5 and Figure 8. A simplified schematic of this error detector is shown in Figure 9. This element of the regulator consists of two input transformers, a twin triode tube, capacitors, resistors, and other components required for the d-c power supply.

Figure 9. Simplified circuit of error detector

The isolating transformers 3T and 4T prevent leakage currents and effects from spurious magnetic fields from interfering with regulator performance. The error signal appearing across the cathode resistors is filtered and a portion then amplified

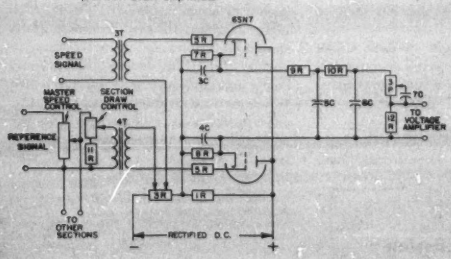
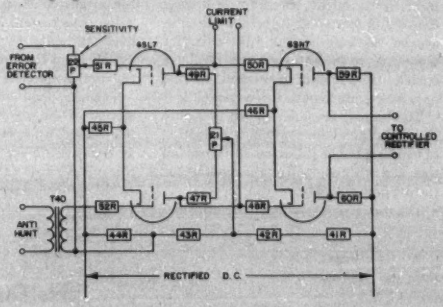


Figure 10. Simplified schematic diagram of voltage amplifier showing sensitivity control as well as antihunt and current-limit feed-back circuits



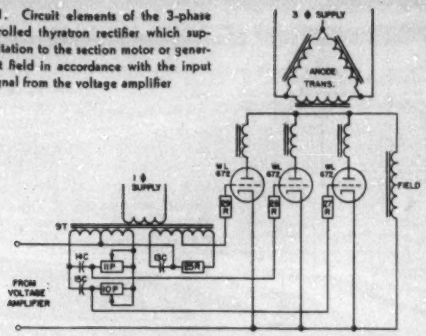


Figure 11. Circuit elements of the 3-phase grid-controlled thyratron rectifier which supplies excitation to the section motor or generator shunt field in accordance with the input signal from the voltage amplifier

The master speed control rheostat, together with the draw adjusting potentiometer are set to provide a 60-cycle reference voltage corresponding to the desired speed for the section. This voltage is applied to the grid of one of the sections of the twin triode tube through an isolating transformer. The a-c tachometer voltage is connected through an isolating transformer to the other grid of the twin triode tube. Pulsating d-c voltages appearing across the two cathode resistors are proportional to the a-c signal voltages. When the tachometer and reference voltages are equal, the current through the two sections of the tube are equal and the voltage output of the error detector is zero. The pulsating d-c voltages are filtered, then compared and the difference voltage again filtered to provide a d-c error voltage which is proportional to the deviation from the desired speed of the section motor. The error signal is modified in value depending upon its rate of change. This provides a field forcing action for rapid changes in section motor speed.

Use of the isolating transformers precludes the possibility of erratic performance which might result from leakage currents or spurious magnetic fields in or around the long conduit runs from the section drive location and the regulator cubicle. As a further precaution, internal impedances are kept as low as possible.

The voltage amplifier shown in Figure 10 is two stage direct coupling consisting of two twin triode tubes, resistors, capacitors, and other components as required for the d-c power supply.

The d-c error signal from the error detector is applied to a potentiometer from which a portion of the error voltage is used in order to control the sensitivity of the system. The selected portion of the error signal is then applied to one section of the twin triode tube which causes the output of one section to increase or decrease depending on the polarity of the input voltage. The other section of this twin triode tube is connected to the antihunt or negative feed back circuit. The antihunt voltage is proportional to the rate of change of motor field current or generator voltage (depending on the system used) resulting from the regulating action, and is in a direction to oppose any change in these quantities. The output of the two elements of this twin triode tube are approximately balanced when no error signal exists.

The second stage also includes a twin triode tube which further amplifies the output of the corresponding section of the first stage. Current limit control can be introduced in this stage. This may be accomplished by measuring the magnitude and polarity of armature current, amplifying this signal and feeding it into the input of the second stage. If the motor armature current exceeds a preset value, the current limit signal will predominant over the error and antihunt signals from the first stage thus limiting the regulator action.

The amplified output of each triode section of the second stage is connected in a bridge arrangement and the positive or negative difference is applied to the controlled rectifier.

The controlled rectifier shown in Figure 11 consists of three thyratrons, phase shift circuit, and transformers. The phase shift circuit is designed to shift the voltage on the grids of the thyratrons 120 degrees lagging with respect to their anode voltages.

The d-c voltage from the amplifier is applied to the grids in series with the fixed phase shifted voltage. Variation of the d-c component of this grid voltage controls the output of the thyratrons over their full range from minimum to maximum. The 3-phase rectifier output is then applied to the field of the section motor or section generator depending on which system is used. The speed of the section motor is thus controlled by the output.

The decision to use a controlled rectifier for motor or generator field excitation rather than a rotating machine was based on the desire to eliminate all possible commutating machines with their attendant maintenance and inconsistencies from the regulating circuit. More important, perhaps, was the aim for the quickest possible regulator response.

Conclusions

The all-electronic regulating system has been applied to a number of machines producing widely different types of paper including tissue, book, and kraft. Operating experience indicates that the overall system design is sound. The use of an a-c speed cue permits isolation of circuits which are often troublesome. The electronic exciter for motor or generator excitation eliminates the necessity of a multiplicity of rotating units in the regulating system.

The sag-back problem is no longer a menace to successful operation of high-speed kraft machines. Quick responding electronic regulators have measured up to the task.

As with all precision equipment a carefully planned maintenance program is essential to the continued high-level performance of which these systems are capable. The most important part of such a program is periodic testing of all high vacuum tubes. Specific limits should be set on tube characteristics and all tubes falling below these limits should be discarded. New tubes should be tested before they are installed. Records of tube characteristics as well as operating life and reason for replacement should be kept.

No Discussion

System Operation Improved With Carrier Telemetering and Load Control

A. W. WALTON
MEMBER AIEE

H. W. LENSNER
ASSOCIATE AIEE

THE POWER systems in the Southwest were banded together during the war to provide adequate capacity for supplying energy to large war industries located there. To insure delivery of power and to assist the dispatchers in maintaining schedules and co-ordinating distribution of generation throughout the area, certain sections were equipped with extensive carrier communication, telemetering, and automatic load control facilities.

In the Oklahoma district, operating companies were set up as one operating area. Similarly, areas in adjacent states were delineated as determined by engineering considerations.

Since the war, the need for co-ordination between areas has been supplemented by the need to properly co-ordinate and control inter-system interconnections within the areas originally set up. In place of co-operating to supply a common load, it has become increasingly necessary for each system to carry its own loads. Effective scheduling of hydro and thermal generation could only be obtained by giving the system load dispatcher the proper load information at strategic points on the system.

Transmission System

The Oklahoma Gas and Electric Company transmission system extends through the central part of Oklahoma from the Texas border to the Kansas border, also east into the state of Arkansas. Figure 1 shows a block diagram of the power systems of Oklahoma and interconnections. There are eight interconnections; one with the Kansas Gas and Electric Company, another with the Southwestern Power Administration, two with Grand River Dam Authority, and four interconnections with the Public Service Company of Oklahoma, most of which are closed for normal operation.

Problem

In order to provide smooth and efficient operation of this power system, the following features for a telemetering and load control system were required:

1. Load information on generating stations and tie lines.
2. Provision for totalizing net load on all interconnections.
3. Load control based on tie line bias or system frequency.
4. Communication facilities between load dispatcher and generating stations.
5. Provision for adding telemetered quantities to allow for expansion of power system.

The Telemeter System

The eight interconnections, an intrasystem tie line and four of the largest generating plants are telemetered into the load dispatcher's office located in Oklahoma City. Power line carrier channels are used extensively because of the distances of transmission.

In order to obtain a co-ordinated system of control, the tie line recorders in the load dispatchers office are arranged so that the net or algebraic sum of all interconnections is continuously recorded, and that the net or any combination of tie lines can be controlled. The latter requires a separate tie line bias recorder.

Figure 2 shows a simplified diagram of the recorders and control. The re-transmitted output of the tie line recorders is fed into the net interchange recorder. Another set of re-transmitters on the tie line recorders, the output of which can be switched, is fed into the tie line bias recording controller. The system frequency recorder contains the controls for bias settings.

The generating plant recorders A, B, C, and D are also shown. The manually-operated potentiometer E simulates the output of small nontelemetered generating plants. The output of the net interchange recorder and the generating plant load recorders is fed into the net system load recorder.

The master load frequency controller is

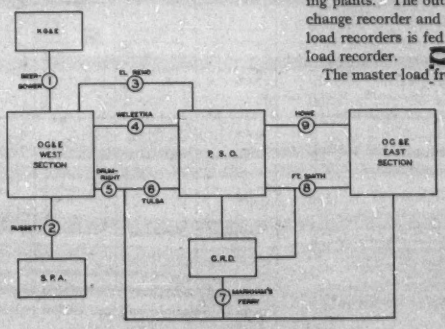


Figure 1. Block diagram of Oklahoma power systems

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A. W. WALTON is an electrical engineer with the Oklahoma Gas and Electric Company, Oklahoma City, Okla., and H. W. LENSNER is a design engineer with the Westinghouse Electric Corporation, Newark, N. J.

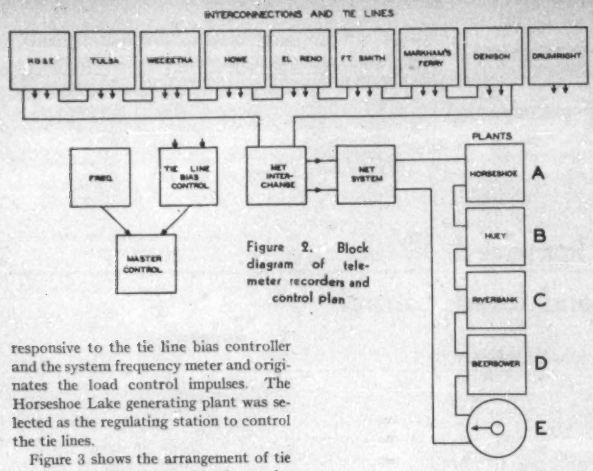

Figure 2. Block diagram of telemeter recorders and control plan

responsive to the tie line bias controller and the system frequency meter and originates the load control impulses. The Horseshoe Lake generating plant was selected as the regulating station to control the tie lines.

Figure 3 shows the arrangement of tie line, generation and system load recorder panels and control equipment. The load dispatcher's recording and load controlling equipment as finally adopted incorporates the following general features:

1. Operates under different types of control, including frequency control when the system is isolated, flat tie line and tie line bias control.
2. Is able to control the net interchange on any tie line or combination of tie lines.
3. Has a wide range of adjustments in the load control equipment.
4. Has a wide range in bias settings.
5. Provides a continuous record of the net interchange.
6. Provides a record of the net system load.

Terminal Telemeter Facilities

The interconnected tie line terminals are equipped with high rate impulse telemetering transmitters for controlling the tone modulated carrier transmitters. In the generating plants the output of high-speed 3-phase thermal devices connected in each generator metering circuit is totalized in a total station load recorder which originates the impulses and controls the carrier transmitter.

The station load recorder has the following features:

1. Provides a graphic record of the total station output.
2. Integrates the total station output.
3. Originates the telemeter impulses to control a carrier transmitter.
4. Controls a boiler room total station load indicator or indicators.
5. Controls a meter which records the station hourly demand.

Carrier Considerations

From a consideration of the extent of the system over which the telemetered quantities had to be transmitted, power line carrier was decided upon for the channels to transmit information to the load dispatcher at Oklahoma City. Figure 4 shows the distances between stations on the power system. The total distance from the most remote substation to the load dispatcher is about 270 miles and includes seven line sections requiring one radio-frequency by-pass at Reno Street. Also, at each station there are one or more other line sections connected to the bus which will divert some of the carrier energy away from the desired channel. The carrier energy divides roughly in proportion to the number of lines radiating from a station bus, so for example, if there are four lines connected to a bus, only one-fourth of the carrier energy will go down the line toward the intended receiving terminal. This applies only to electrically long, nonresonant lines. For short lines an odd-quarter wavelength long, the carrier losses may be very high.

Numerous transformer banks are located along the lines and at the various stations, and while in general a power transformer does not absorb appreciable carrier energy, there are so many that they cannot be neglected. For example, between Fort Smith and Riverbank (including the line and two station busses) there are 12 transformer banks. Several of the line sections have short taps on them and these in particular restrict the choice of frequency.

A survey of the use of the 50–150 kc carrier band in the area disclosed the fact that this range of frequencies was being utilized very nearly to capacity and left insufficient channels for the telemetering system contemplated. Actual monitoring at widely separated points confirmed this fact. In view of this condition, it was necessary to go outside the 50-150 kc band for sufficient channels. Frequencies as low as 30 kc were being used, and the majority of the telemetering signals are between 150 and 250 kc, with provisions for going as high as 300 kc if necessary.

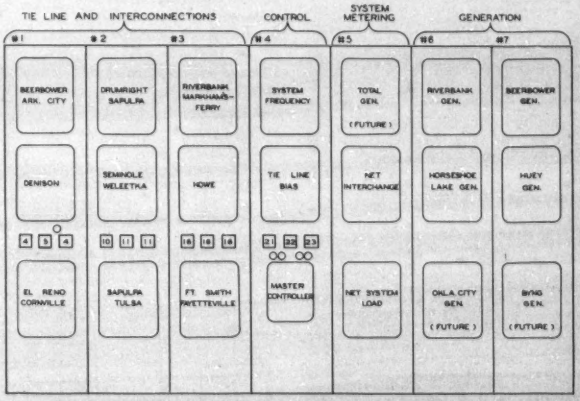

Figure 3. Telemeter and load control panels at load dispatcher's office in Oklahoma City

Since attenuation increases with frequency, it was apparent that satisfactory operation of single carrier circuits over as much as 270 miles of the system would be unsatisfactory with standard carrier equipment because there would be insufficient margin between the calculated circuit attenuation and the maximum capability of the equipment. High power transmitters were considered, but this would be undesirable because of excessive radiation at the higher carrier frequencies, which might result in interference with other services. In view of these facts, it was decided not to transmit carrier signals from the most remote stations all the way to the load dispatchers at Oklahoma City without repeat stations along the way where the signal levels could be boosted. The total distance of 270 miles from the most remote point with six intermediate stations and numerous additional lines diverting carrier energy would give a poor signal-to-noise ratio at the receiving end if only a single carrier channel were used for the entire distance.

This problem was met by using standard carrier transmitters and two repeat stations to maintain a favorable signal-to-noise ratio. In addition, a line trap was used at Riverbank to isolate lines and busses which otherwise would have absorbed a large percentage of the carrier energy.

In order to utilize each carrier channel as much as possible, tone modulation of the carrier signal was employed so that several telemetered quantities could be transmitted over a single carrier channel.[1] In this way the number of quantities that could be transmitted in a given channel space was greatly increased. Even in cases where only a single quantity was transmitted from a station, audio tones were used for uniformity and to allow for future expansion.

With a carrier system employing tone modulation, there is also the possibility of doubling the available carrier channels by conversion to single sideband modulation.[3,4] This provides for future expansion of the telemetering system without rendering the existing carrier equipment obsolete. The same carrier components are used, and it is necessary only to add the units which will convert a conventional double sideband amplitude-modulated carrier system to single sideband operation. Whereas a conventional amplitude modulated carrier channel employing ten tones with frequencies up to 2,900 cycles requires a channel approximately 6 kc wide, with single sideband equipment the same information can be transmitted over a channel only 3 kc wide, and with an improved signal-to-noise ratio. This is brought about because in the single sideband system all the carrier energy is in the sidebands, and the base carrier frequency is supplied at the receiving terminal from a stable oscillator.

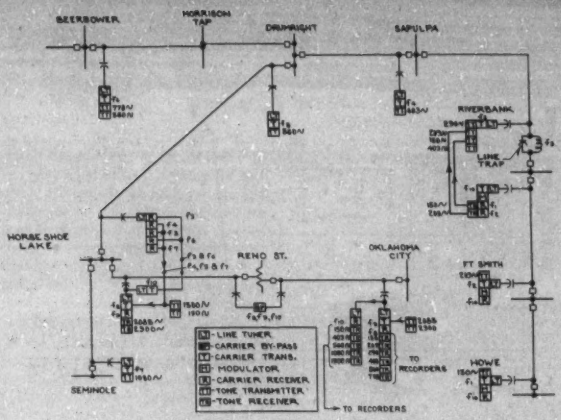

Figure 5. Block diagram of carrier and tone channels for communication, telemetering, and load control

The System Adopted

Figure 5 shows a block diagram of the essential elements of the carrier telemetering system. From Howe and Fort Smith, 150- and 209-cycle tone modulated signals of two different carrier frequencies (*f1* and *f2*) are transmitted to Riverbank where they are received on two carrier receivers whose outputs are connected in parallel to energize two tone receivers. The output relays of these two tone receivers in turn control two tone transmitters of the same frequencies (150 and 209 cycles) which modulate another carrier signal (*f3*) transmitted to Horseshoe Lake. In addition, 290-cycle and 403-cycle tones keyed by the quantities to be telemetered from Riverbank are impressed on the same carrier frequency, to Horseshoe Lake, which now contains the telemetering signals from Howe, Fort Smith, and Riverbank.

This method of repeating has the advantage of eliminating all noise that may be present in the output of the receiver whose signal is being repeated. The tone receiver relay keys another tone transmitter, and essentially the signal starts all over again on a different carrier frequency.

Provision has been made in this part of the system for a communication channel between Riverbank, Fort Smith, and Howe, separate from the existing 50-kc carrier communication system. This was provided to handle the load dispatching and reporting in of miscellaneous infor-

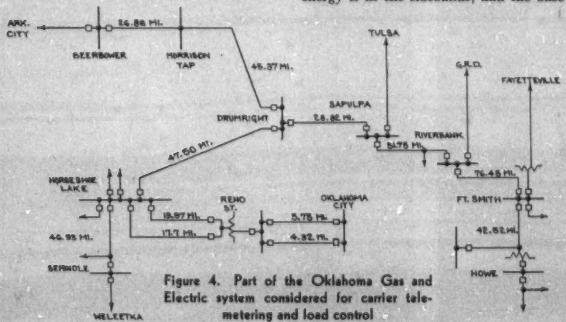

Figure 4. Part of the Oklahoma Gas and Electric system considered for carrier telemetering and load control

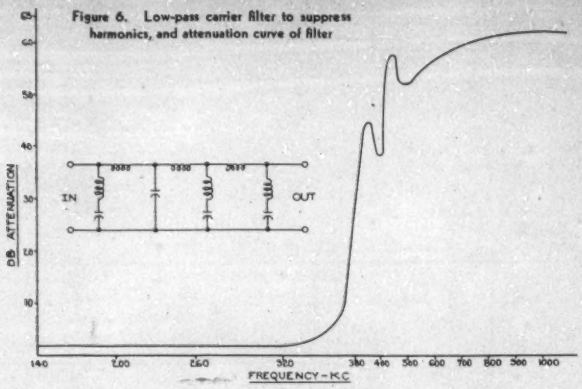

Figure 6. Low-pass carrier filter to suppress harmonics, and attenuation curve of filter

mation to Riverbank from Howe and Fort Smith without tying up the main communication channel.

From most of the other stations on the system, a single quantity is transmitted by tone modulation. Although unmodulated carrier on-off keying could have been employed from each of these stations, provision was made for future expansion where it would be desired to transmit more than one quantity from each station. These stations include Sapulpa, Drumright, and Seminole. Two audio tones are transmitted from Beerbower station. All the signals from the outlying stations are received at Horseshoe Lake on a total of five receivers. The audio frequency outputs of these receivers are connected in parallel in two groups as shown in Figure 5, together with one tone transmitter for the local generation and one tone for repeating Denison Dam interconnection. Each of the two groups is retransmitted on a different carrier frequency (*f8* and *f10*) from Horseshoe Lake to the load dispatcher at Oklahoma City where the final tone receivers and recorders are located.

The question might be raised as to why another repeat station was used with only 25 miles of line remaining to the ultimate receiving point. It will be noticed that there is a high-voltage regulating station at Reno Street, and it is necessary to use a carrier by-pass around the regulating transformer and circuit breakers. The cost and complexity of carrier by-pass equipment for 11 carrier channels would have been prohibitive. Instead, only a double frequency by-pass is needed, and its tuning is broad enough to pass the carrier frequencies for the telemetering signals, and another frequency (*f9*) transmitted from Oklahoma City back to Horseshoe Lake for load control.

After the received telemetered quantities are routed to the respective recorders, they are then totalized for the desired quantity for controlling generation by means of "raise" and "lower" impulses. These impulses control two audio tones which are transmitted over another carrier channel back to the Horseshoe Lake station. The load control impulses are received at Horseshoe Lake through two tone receivers and then routed to the governor of the turbo generator selected for regulation.

Carrier Equipment

Standard commercial power line carrier equipment has been used on this installation. The design of carrier units for the 150-300 kc band presented no particular problems but amounted primarily to a change of the constants of the tuned circuits of the component units. Coupling capacitors, line tuners, and line traps behaved essentially the same as at lower frequencies with negligible increase in losses.

One unit requiring particular attention was the carrier transmitter which must operate with very low harmonic distortion in its output. This is particularly important in the 150–300 kc range since low order harmonics fall in the standard broadcast band. To aid in suppressing harmonic output, a low-pass filter was connected between the output of the car-

Figure 7. Front view of load recorders and control equipment in load dispatcher's office, Oklahoma City

Figure 8. Front view of carrier equipment at Riverbank Station

This assembly includes audio and carrier frequency units for a relay repeat of two telemetered quantities, and for communication with Howe and Fort Smith

rier transmitter and the coaxial cable. This filter introduces negligible loss in the carrier frequency range up to 300 kc, but a high attenuation in the broadcast band, as shown in Figure 6. The filter also has an attenuation peak in the vicinity of 450 kc to prevent interference arising from carrier harmonics getting directly into the intermediate frequency section of broadcast receivers. With the equipment properly installed and adjusted, no radio interference was encountered.

The equipments operate from a-c power supplies to reduce the station battery load. The transmitters have an unmodulated carrier output of $7^1/_2$ watts and are capable of nearly 100 per cent modulation. Two types of receivers are used: superheterodynes for most of the channels, and tuned radio-frequency receivers for short channels where the carrier attenuation is low. Both types of receivers are provided with automatic volume control to restrict the variation in audio output for variations in the level of the carrier signal.

Conclusions

This carrier telemetering and load control system has been in operation for over a year and has effected more efficient and economical operation of the power system because of the load information supplied to the load dispatcher, and the automatic load control feature. A continuous record of the net interchange between systems has been kept, as well as the load on each large plant and the total load of the system. Personnel have been trained for preventive maintenance to insure continuity of operation of the equipment.

Provision has been made to allow for future growth when it will be necessary to add more telemetered quantities and load control channels without making the existing equipment obsolete. The transition from manual to automatic load control on this power system has begun, and the experience gained today will be translated into further improvements in the future.

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2. Operation of Power Line Carrier Channels, H. W. Lensner. AIEE Transactions, volume 66, 1947, pages 888–93.

3. A New Single Sideband Carrier System for Power Lines, B. E. Lenehan. AIEE Transactions, volume 66, 1947, pages 826–30.

4. A Simple Single Sideband Carrier System, R. C. Cheek. *Westinghouse Engineer* (Pittsburgh, Pa.), November 1945.

Discussion

R. W. Beckwith (General Electric Company, Syracuse, N. Y.): As we gain experience with extensive telemetering applications such as this we now see that another type of carrier equipment described in another paper[1] could be applied resulting in a simplified system using much less of the frequency spectrum. Crystal-controlled frequency-shift channels for each telemeter reading could be closely spaced in frequency and the group treated as one carrier channel. A thorough study might show that the Fort Smith and Howe readings would have to be repeated at Riverbank. These two readings could be transmitted to Riverbank on closely adjacent frequencies forming a second carrier channel. With this arrangement no single tube failure could interrupt more than one channel. Independence of automatic volume control on each channel would eliminate any possibility of severe changes in line attenuation disrupting a reading.

Reference

1. Characteristics of New Carrier-Current Equipment for Telemetering and Load Control, R. W. Beckwith. AIEE Transactions, volume 67, part II, 1948, pages 1649–53.

C. W. Boadway (The Hydro Electric Power Commission of Ontario, Toronto, Canada): The paper indicated that a complicated problem of power line carrier for telemetering and load control was quite evident in the system as operated by the Oklahoma Gas and Electric Company. It would appear from the data collected for the paper that the transmission line under discussion was to some extent a distribution type in that numerous taps were involved which did not allow the communication or carrier engineer a straightforward carrier transmission problem such as might be found in a direct terminal to terminal installation. The lack of wave traps in a system of this kind adds to unfavorable possibilities of power carrier transmission under adverse weather conditions.

Under Carrier Considerations, the attenuation for bus coupling was described. It leaves the impression that bus coupling was actually used quite frequently; however, in practice this is not the case. In order to obtain higher efficiency from transmitter-receivers, phase-to-phase and phase-to-ground coupling are used much more extensively.

On the matter of tap lines it was stated that with electrically long tap lines the radio-frequency energy divided equally between the tap and the desired line, and that on electrically short lines considerably high frequency losses may be incurred. However it was not mentioned that a short tap line may be changed to act as an electrically long line by terminating the line by its characteristic impedance, or that its effects may be cancelled completely by using a line trap in the short tap. The particular problem of the length of untrapped tap lines and the choice of the frequency of the carrier may have been included in this discussion. The frequency of the carrier may be so chosen that no excessive losses result by reflection due to the length of the line being an odd-quarter wave length.

On the discussion of the 270-mile telemeter channel, it was emphasized that due to the length of the line the attenuation was so great that it was necessary to use carrier transmitter receivers to amplify the signals. It may be added that since telemetering quantities at an intermediate point would be valuable to system operators, the quantity may be recorded at the intermediate point and then retransmitted. This possibly is being considered for future operations.

D. M. MacGregor (Ebasco Services Incorporated, New York, N. Y.): Walton and Lensner have given us a very clear and concise description of a rather complete installation of tie-line telemetering and load control equipment. As in so many other cases, the initial, limited installation of this type of equipment served to illustrate both the usefulness of the equipment as an operating aid and the need for a more complete installation. Several of the essential features of a well-designed installation, such as a careful choice of quantities to be recorded, are covered very well in the paper. Two or three other points seem to deserve further discussion.

Higher speed telemetering and control equipment has long been considered desirable by many operators. The equipment described in this paper has relatively long over-all response time as compared to some of the newer systems which are or will soon be available. It would be of interest to know if operating experience with the Oklahoma system has indicated the desirability of using the higher speed equipment, especially with respect to control of low-capacity tie lines.

Interconnection telemetering is usually of interest to both of the interconnecting groups and, for that reason, the telemetering system and the carrier facilities should be suitable for use by both companies, if possible. The paper does not indicate whether or not this feature was considered when designing the Oklahoma Gas and Electric Company installation.

The power-line carrier-frequency spectrum is quite limited and, in many cases, crowding of this spectrum is proving to be a serious limitation on further expansion of carrier telemetering facilities. It would be interesting to know the reasoning which dictated the choice of audiotone carrier telemetering for all channels as compared to the frequency-shift system, since the latter system is generally regarded as being very economical in use of channel space for interconnection telemetering. Audio-tone equipment appears to be best suited to those installations where at least three quantities are telemetered from a single point. The greater sensitivity of frequency-shift equipment usually makes it unnecessary to repeat signals at intermediate points so that the complete system develops into one or two groups of closely-spaced channels originating

at numerous remote points and all terminating at the central control and recording station. Under these conditions, the use of carrier-spectrum space is a minimum as compared to the apparently greater use involved with the audio-tone system. Actual frequencies used on the Oklahoma Gas and Electric Company system are not indicated in the paper but it appears that channel spacing must be fairly wide and that the system must use a relatively large portion of the frequency spectrum. It would appear that some saving of channel space would have been possible by use of frequency-shift equipment at least on all channels originating west of Riverbank.

A. W. Walton and **H. W. Lensner:** The authors are well pleased to see the interest shown in the subject of carrier telemetering. While power line carrier is well established as a medium for telemetering and other functions, each application uncovers new problems in setting up a reliable channel, determining the type of carrier transmission (keyed, modulated, and so forth) and utilizing the quantities received. This ever-changing picture presents a constant challenge to the engineer of both the manufacturer and the user of the equipment.

The discussers have shown much concern regarding the crowding of the carrier spectrum, attenuation and line traps, and rightfully so. The problem became apparent when it was found that the 50–150 kc carrier spectrum was already well utilized in an adjacent public power area by many existing duplex carrier communication installations which paid the price of channel space for the duplex feature. This greatly restricted our choice of frequencies, and as stated in the paper, we were forced to go into the 150–300 kc spectrum. We refer Beckwith to our paper describing the repeater stations at Riverbank and Horseshoe Lake. Tube failures are negligible. For Boadway's information, the telemetered quantities repeated at Riverbank were also diverted for local use.

MacGregor is quite correct; the initial installation was limited because of inability to obtain adequate equipment during the war. Our program which is now under way includes line traps wherever necessary and the gradual elimination of bus coupling and its attendant high carrier losses resulting from diversion of carrier energy from the desired path.

The points MacGregor raised are of interest. Higher speed telemetering and control equipment would be very desirable, particularly for controlling our low capacity tie lines. A neighboring utility does make use of some of the telemeter channels. As will be noted, the Horseshoe Lake terminal serves as a gathering point for all telemeter quantities and thence is repeated by audio tones over two carrier channels to the Oklahoma City terminal. Economics dictated audio tone equipment for the latter section. Likewise, when the system is expanded to include more telemeter quantities such as reactive kilovolt-amperes, it will be economically feasible to do so by merely adding more tone units to the existing carrier transmitters and receivers. The carrier system has given a very good account of itself and the telemetering and load control have become an indispensable tool in system and interconnected operation.

Characteristics of New Carrier-Current Equipment for Telemetering and Load Control

R. W. BECKWITH
ASSOCIATE AIEE

THE RAPIDLY expanding use of telemetering by power companies is made possible largely by carrier-current channels. Anticipating the requirements of present day telemetering needs a narrow-band frequency-shift type of equipment was developed. This equipment has received wide acceptance and the many channels which have been in successful operation for some time have confirmed the value of the design features.

A review of the forms of telemetering now in use with carrier-current channels shows that in some cases the carrier transmitter must be controlled by a single opening and closing contact and must duplicate this contact performance at the receiving end. In one frequency type of telemetering[1] the carrier transmitter must accept a variable, low-frequency alternating voltage and must reproduce this voltage at the receiver output terminals. In order to make possible the use of the maximum number of carrier-current channels on a given power system, it becomes very important to conserve the carrier-frequency spectrum and utilize the minimum possible power. Since many telemeter channels are required to operate over long distances, the equipment must be capable of operation through high attenuation with good independence from noise, interfering signals, and wide changes in attenuation. This paper describes a new type of carrier-current equipment which fulfills these requirements.

The substantiated theories of frequency modulation[2] have been applied to the signals from 0 to 30 cycles per second as required for telemetering. This shows that the use of a very narrow-band frequency-shift system[3] will provide the greatest useful range per unit of transmitted power.

Of equal importance to range, these principles indicate a more economical use of the carrier frequency spectrum and excellent independence of received telemeter signal from variation in received carrier voltage.

Quartz crystal circuits are used to generate the frequency-shift signal and to detect the small (0.06 per cent) shift in transmitted frequency. The receiver uses a crystal filter and crystal discriminator. One type of transmitter uses two crystals and a means for switching output. Another type uses a single crystal and reactance tube circuit for shifting frequency.

An important feature of the equipment is the independent operation of each channel, while at the same time many channels can operate through common coupling and tuning equipment. As many as 25 channels can be accommodated in a 12 per cent wide frequency band. A valuable flexibility results from the fact that it is not necessary for all transmitters of a group to be located at one point, making it possible to transmit a number of telemeter readings from several outlying stations to a central control point, all in the same band of frequencies. It is possible to transmit two groups of telemeter frequencies in opposite directions with 5 per cent separation between groups. Unidirectional channels in each group are spaced 0.5 per cent apart.

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R. W. Beckwith is with the carrier-current section, transmitter division, General Electric Company, Syracuse, N. Y.

This material is part of a thesis presented to the Faculty of the Graduate School of Syracuse University in partial fulfillment of the requirements for the degree of Master of Electrical Engineering.

General Objectives

Carrier-current equipment has been developed to meet the following objectives, keeping in mind the requirements of the telemetering instruments in common use.

1. The equipment should require the minimum frequency space consistent with necessary bandwidth required by the nature of the signal.
2. It should make possible the use of low-power transmitters and maximum operating range by obtaining the best possible freedom from effects of power-line noise.
3. The greatest reliability should be obtained by making the operation of the equipment essentially independent of line attentuation variations.
4. It should provide maximum versatility and reliability by making each channel independent as far as electronic circuits and eliminating any requirement that all transmitters be at one spot and all receivers at another. For economic reasons, it should make possible maximum joint use of coupling and tuning equipment.

Fulfillment of the first two objectives directly contributes to reducing frequency congestion. Fulfillment of the fourth objective results in greatly simplifying a system requiring a number of telemeter readings to be brought from several outlying points to a central dispatcher's headquarters.

These readings can be transmitted on closely adjacent frequencies, and the entire group treated as one "wide band" carrier channel, rather than separate "wide band" channels from each outlying station. Often a reading can be transmitted on one frequency that otherwise, because of the large attenuation, would have required two tandem channels on separate frequencies.

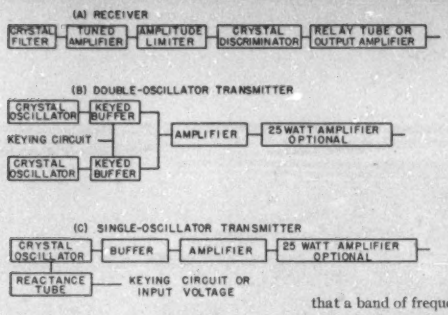


Figure 1 (left). Block diagrams of receiver and two types of transmitter

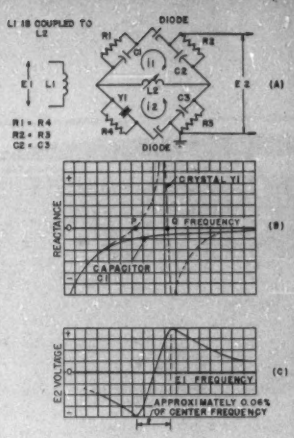


Figure 2. Crystal discriminator (A) circuit diagram (B) crystal characteristic, (C) discriminator circuit characteristic

Frequency-Shift Operation Selected

Frequency-shift type of transmission was chosen, since at the receiving point two steps can be taken (in addition to frequency selection) to provide greatest freedom from the effects of line noise. The first, an amplitude limiter,[2] well understood from its use in frequency-modulation receivers, also provides independence from variation in line attenuation up to the full operating range of the equipment. The second, a discriminator,[3] also well understood from its use in frequency-modulation receivers, provides excellent cancellation of noise. This latter circuit could be thought of as analogous to a polarized relay which will not respond to rapidly fluctuating interfering currents but will move one way for a steady current in one direction, and move the other way for a steady current in the other direction.

Crystal Operation

The transmission of such a recurring function as a telemeter signal can be analyzed on much the same basis as voice transmission. For voice, a band of frequencies from about 300 to 3,000 cycles per second must be transmitted. The usual telemetering signal, whether impulse or frequency-type, is made up of very low-frequency components with a maximum value of about 30 cycles per second.

One type of impulse duration telemetering uses a total cycle of 2.4 seconds. A Fourier analysis of such a signal will show that the lowest frequency to be transmitted is considerably less than one cycle per second. The shortest impulse to be transmitted is 0.1 second, equivalent to 5 cycles per second. In order to approximate a square impulse some third harmonic or 15 cycles per second should be transmitted. We may therefore conclude that a band of frequencies up to 15 cycles per second must be transmitted. In another frequency-type telemetering,[1] a 6-to-27 cycle-per-second band is required.

It has been demonstrated by those working with frequency-modulated voice transmission,[4] that the greatest range is obtained by a deviation ratio of one. (Deviation ratio is the ratio of frequency swing each side of the carrier frequency, to the highest modulating frequency.) Since the same principles must apply for the lower frequencies below 30 cycles per second, an optimum range for telemetering would require a frequency shift of about 60 cycles.

The very narrow bandwidth desired strongly suggested the use of quartz crystals as this was the only practical circuit element which combined the necessary stability and high "Q" requirements. As a further contribution to the elimination of frequency congestion between 50 and 150 kc, the equipment has been designed to cover a range from 30 to 200 kc. The equipment is designed for two frequency ranges, one 30 to 90 kc and the second 90 to 200 kc. "Flexure mode" crystals, requiring slightly different circuits, are used below 90 kc, resulting in excellent stability while at the same time permitting a large reduction in size over more conventional cuts.

The Receiver

Figure 1(A) is a block diagram of the receiver, a tuned radio-frequency design chosen for simplicity and stability. This receiver uses a crystal filter ahead of the first amplifier tube. This filter was especially designed to provide the band-pass characteristic necessary to pass the two transmitted frequencies (high-shift and low-shift) and at the same time give maximum selectivity above and below the operating frequencies. Since this crystal filter is placed before the first tube, the greatest part of the frequency selection is performed before any undesirable mixing of frequencies can take place due to nonlinearity in the first tube. The crystal filter is followed by two double-tuned radio-frequency circuits with the associated amplifier tubes, an amplitude limiter stage, a crystal discriminator stage, and either a relay tube or a low-frequency amplifier.

The circuit which is the heart of the proposed system is the receiver discriminator. It can be seen from Figure 2(A) that the crystal discriminator circuit consists of a bridge which is symmetrical with the exception of crystal *Y1* and capacitor *C1*. From the reactance curves of the crystal and capacitor, shown in

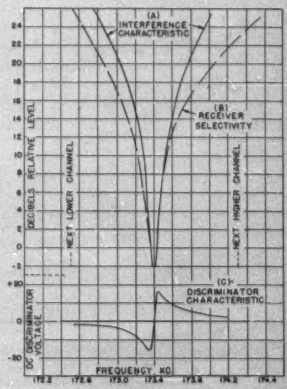


Figure 3. Receiver operating characteristic

Figure 4. Assembly of eleven parallel-connected carrier-telemeter receivers

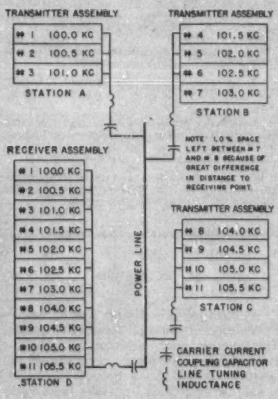


Figure 5. Block diagram of radial type of application

Figure 2(B), it can be seen that these also balance above and below the resonant region of the crystal. At point P, the crystal has a low resistive impedance and so the current $i2$ exceeds current $i1$, and a negative output voltage results. At point Q, the crystal has a high resistive impedance and the current $i1$ exceeds $i2$ and a positive output voltage results. The resultant characteristic curve is shown in Figure 2(C).

The unique features of this circuit are the very narrow frequency spectrum used (about 0.06 per cent between positive and negative peaks) and the fact that the frequency occurrence of the peaks is almost entirely controlled by the crystal itself. Since for greatest stability it is desirable to shift the transmitted frequency from the positive to the negative peak, the crystal discriminator fixes the frequency shift to about 0.06 per cent. Although this shift is not exactly the same at all carrier frequencies, crystals may be duplicated at any one frequency allowing replacement of crystals and allowing the operation of more than one receiver from one transmitter.

Figure 3 shows three characteristic curves taken on an actual receiver. Figure 3(B) is a selectivity curve from receiver input to the limiter grid and includes the crystal filter selectivity plus the selectivity of the tuned amplifier. This curve shows the relative input necessary to maintain constant limiter grid current as a function of frequency and was taken with automatic volume control off. The curve passes through zero decibels at the high-shift and low-shift transmitted frequencies. Figure 3(A) was taken by introducing an interfering signal into an operating channel. The ratio of interfering signal to desired signal voltage at the receiver input terminals is plotted at the threshold of interference to the receiver. In the region above the curve, therefore, operation of the channel is impossible, but below the curves operation is unaffected by the interference. The improvement of this curve over Figure 3(B) is due to the additional selectivity of the crystal discriminator circuit. Figure 3(C) shows the crystal discriminator output voltage as a function of frequency, plotted to the same frequency scale as Figure 3(A) and Figure 3(B).

Table I

Total Number Transmitters Connected in Parallel	Decibel Reduction of Operating Range
2	6
3	9
4	12
5	14
6	16
7	17
8	18
9	19
10	20

The Transmitter

Figure 2(B) is a block diagram of one type of transmitter which utilizes two crystals ground to match the positive and negative peak of the associated receiver discriminator characteristic. These crystals operate continuously in separate oscillator circuits and each drives an associated buffer stage. Keying of the buffer stages therefore causes a shift in output frequency, and produces a positive or negative output voltage from the associated receiver discriminator. In order to accommodate keying either by a voltage or by a keying contact, another transmitter shown in block diagram in Figure 1(C) is used. This transmitter makes use of a single quartz-crystal oscillator which includes a reactance-tube circuit in such a way that this circuit will cause the necessary small shift in output frequency. By applying the keying voltage to the reactance-tube grid circuit the carrier frequency can be varied in accordance with this voltage.

Application

Groups of these transmitters may be spaced at 0.5 per cent intervals of fre-

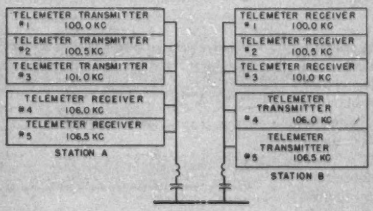
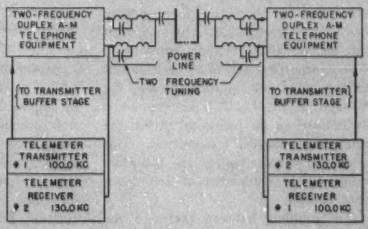


Figure 6. (left) Block diagram of 2 - directional type of application

Figure 7. (right) Block diagram of combination of a 2-frequency duplex telephone channel and a telemeter channel in each direction



quency. A space of either 0.5 per cent or 1.0 per cent may be left between adjacent channels of two different groups of transmitters depending upon the application. Two-direction channels are possible by leaving a 5.0 per cent spacing between groups of channels transmitting in the two directions.

This equipment is extremely versatile in its application as shown by the following typical examples.

Multistation Radial Application

It is often desirable to transmit telemeter readings from a number of generating points, inter-system tie points, and so forth, to a central dispatching station of a power company. Figure 4 shows such an equipment now in operation. One or more readings may be received from each such point and utilized individually, or they may be totalized in various combinations. In addition, a particular reading or some form of automatic load control signal may be transmitted from the central dispatching station to one or more generating points. Crystal-type carrier telemetering equipment lends itself very well to such an arrangement in that as many as 25 such channels may be grouped in a 12 per cent wide frequency band and handled as one carrier frequency using single-frequency transmitter and receiver tuning equipment, by-passes, and line traps.

All receivers are merely connected in parallel with the line tuning adjusted for a mean frequency, and with each receiver input circuit adjusted to the frequency of its associated transmitter. The major part of the frequency separation is not achieved by the receiver input circuits, however, but by the crystal filter circuits. Figure 5 shows a typical assembly of eleven receivers all operating in parallel on the same channel.

All transmitters at each transmitting point are connected in parallel and tuned to the line by single-frequency tuning units. When connected in parallel, each output amplifier tube has impressed on its plate the voltages of each of the other transmitters. It is reasonable to require that the total plate voltage swing, with two or more transmitters in parallel, must not exceed that for one transmitter. This means that the total peak output voltage must remain constant and the voltage output of each transmitter must be reduced as the reciprocal of (n), the number of parallel transmitters. This results in a reduction of the operating range of the equipment in accordance with Table I.

The coupling between plates of the various amplifier tubes is less than unity and the realized losses are smaller than those indicated, therefore, Table I is very conservative. Of course, no reduction need be made for adjacent channels located at some other transmitting point because of the separation between transmitters afforded by line attenuation. Each transmitter group is therefore considered only on the basis of the number actually connected in parallel at one station.

Although the laboratory range of the equipment is in excess of 100 decibels a conservative operation range of 85 decibels (with a 2-watt transmitter) has been assigned as an application guide under average line noise conditions.

Two-Direction Arrangement

Two-directional channels can, of course, be arranged by conventional 2-frequency tuning of the coupling capacitors at each end of the line. With the high selectivity afforded by the crystal equipment an arrangement such as Figure 6 can be made which is economical both in cost and frequency spacing. A transmitter and receiver may be connected in parallel at each end and tuned by common single-frequency line-tuning equipment. In this case the transmitter is decoupled by means of a small resistance. A frequency spacing of 5.0 per cent is used between frequencies and the equipment when so used is rated at 70 decibels with a 2-watt crystal-controlled frequency-shift transmitter. Additional channels may be added in either or both directions at a spacing of 0.5 per cent above the higher or below the lower of the first two frequencies. A further reduction in decibel rating is made according to Table I on the basis of the transmitters actually tied in parallel at one or the other end of the line. (If the number of channels in each direction is not the same, the decibel rating might differ for opposite directions.) In a 12 per cent frequency bandwidth as many as eight channels can be transmitted in each direction with single-frequency tuning. In fact similar schemes involving more than two stations can be worked out but usually require special consideration.

Combination with a 2-Frequency Telephone Channel

As shown in Figure 7, it is possible to replace the carrier-frequency oscillator of an amplitude-modulated transmitter used in a 2-frequency telephone channel by the output of a frequency-shift telemeter transmitter and thereby provide simultaneous telemetering in either or both directions. By using the reactance-tube type of telemeter transmitter (Figure 1(C)), the shift in frequency is so smooth that no interference is caused in the voice receiver, which responds only to amplitude changes. The telemeter receiver and voice receiver are placed in parallel. Because the telemeter receiver has such sharp selectivity, voice modulation does not affect the telemeter receiver. The telemeter and telephone functions can go on at the same time, entirely independent of each other.

It is also possible to add a low-pass filter in the carrier-current voice receiver output (cutting off at about 3,000 cycles per second) and add a second telemeter channel above and a third below the telephone channel starting 4,000 cycles from the center telephone channel frequency.

In such a combination as described, it is suggested that the first telemeter channel always be used to replace the voice transmitter oscillator because this provides crystal control for the voice frequency and prevents change of this frequency from any cause including "knob twisting" by unauthorized personnel.

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Discussion

C. W. Boadway (The Hydro Electric Power Commission of Ontario, Toronto, Canada): The paper definitely indicated an improvement in general frequency requirements as approximately 25 channels can be operated in a 12 per cent wide frequency band thus conserving important carrier frequency allocations.

The paper indicated improved carrier transmission properties as follows:

1. Improved signal to noise ratio (good selectivity).
2. Independent of line changes (limiter and ratio detector).
3. Frequency shift operation (0.06 per cent shift).
4. Independent operation of each telemeter channel.
5. Operates through a common coupling or tuning unit.
6. Spacing of 0.5 per cent for unidirectional channels.
7. Spacing of 5 per cent for bidirectional channels (duplex).
8. Operates well over 85 decibels with 2-watt transmitter.

9. Handles square waves. Pulse duration and so forth up to 10 cycles per second passing three harmonics, that is the bandwidth 30 cycles per second (±30 cycles per second deviation).

10. More than one receiver can operate on one frequency (matched crystals).

11. The receiver stability is essentially independent of all variations except the crystal drift which is negligible.

The paper in general indicated that the multistation radial application up to 25 remote transmitters can transmit intelligence to a central point without exceeding a bandwidth of 12 per cent. It also indicated that at the central station, only one coupling and tuning unit is required and one set of coupling equipment at each group of transmitters. At a 2-directional station, conventional 2-frequency tuning units and coupling capacitors are required. If the line loss is not over 70 decibels and 5 per cent frequency spacing, one 2-watt transmitter with receiver can operate on duplex over a single-frequency line tuning unit. This would also include a maximum of eight channels working duplex over a single-frequency tuning unit.

The application of close coupling channels requires the use of precision type crystal frequency drift, and special attention would of necessity be employed in the tuning of wave traps, particularly in conjunction with the 25 transmitter channels which have a total width of 12 kc.

It is clear that the author and his associates in the General Electric Company have given considerable time and study to the narrow-band frequency-shift type of operation and it should contribute greatly to the users of power line carrier equipments, particularly where numerous channels are involved, thereby reducing carrier frequency requirements.

D. W. Mercer (Ebasco Services Inc., New York, N. Y.): This paper describes an improved type of frequency shift equipment based upon principals of frequency modulation and employing a frequency shift of only 0.06 per cent and capable of operating with only 0.5 per cent separation between channels.

This feature should appeal to those who have the problem of adding telemetering and load control channels on systems where the carrier spectrum is already crowded and where maximum use must be made of the available band widths.

The narrow band widths required and the use of frequencies above and below the 50-to-150 kc carrier range are features which do much to conserve the carrier frequency spectrum. While this appears to be the greatest advantage of this equipment over that previously available, it has many other desirable features including good independence from noise or signal interference and attenuation variations.

For benefit of those not familiar with operation of several transmitters through common tuning and coupling equipment, it might have been helpful if the author had covered this subject more fully.

The title of the paper indicates that the equipment is suitable for both telemetering and load control. However, the paper describes the application of the equipment for telemetering. Some mention of application for the transmission of load control signals might have been desirable, especially if modifications are made in the equipment when it is used for this purpose.

R. W. Beckwith: The operation of several transmitters or receivers involves no particular problems since all transmitters or receivers are merely tied in parallel and then treated in all tuning and coupling circuits as though there was only one transmitter or receiver.

A somewhat special, but at the same time simple and straightforward, tuning procedure for the individual parallel connected transmitters has been worked out. The individual receivers are tuned exactly as if the other receivers were not present.

This equipment is already in use by the Tennessee Valley Authority for the transmission of a load control reading, in which case the reading is merely an integrated telemeter quantity. A load control application may require the transmission of both a raise and a lower impulse, and if so, requires special consideration.

A question has been brought up concerning the type of crystals used. These are of various cuts depending upon frequency. The choice is governed by the desire to obtain small size and at the same time good temperature coefficient. These crystals are silver plated and mounted in an octal metal tube shell. The shell is filled with dry nitrogen and hermetically sealed. This mounting is probably the most stable type in general use today.

Channels for Operation of Supervisory Control Equipment

W. A. DERR
MEMBER AIEE

M. J. BROWN
MEMBER AIEE

A SUPERVISORY control channel consists of a transmission medium and channel terminal equipment. The existing installations of supervisory control employ metallic circuits as the transmission medium. The metallic circuits in use are either telephone line wires or power lines. For operation over telephone line wires, pulses of direct current are generally used to transmit the desired information. When a power line is the transmission medium for operation of supervisory control equipment, a power-line carrier frequency or subcarrier is utilized.

Consideration is now being given to a new type of supervisory control channel making use of space as a channel transmission medium. The advent of micro-wave radio equipment has renewed interest in the use of the hitherto impractical radio channel for operation of supervisory control equipment.

Operation by d-c pulses may be effected over open construction telephone lines or telephone cable wires, either of which may be privately owned or leased. The service record of this type of operation has been exceptionally good through a period of 25 years. Cable wires are less susceptible to lightning strokes, induced voltages between wires, and crossovers with high tension lines, and are therefore preferable to open-wire lines. However, very effective protective equipment for open-wire lines is in common use for the protection of equipment and personnel.

A power-line carrier channel is used usually where the cost of the power-line carrier terminal equipment is less than the cost of establishing a telephone line. There are also instances where an economic advantage can be obtained by having supervisory control share a power-line carrier channel with other services. A very satisfactory operating record of supervisory control over power-line carrier channels has been established during the past 12 years.

While there are no commercial installations to date, the practicability of the use of micro-wave radio channels for the operation of supervisory control equipment has been established. Promise is held for this type of channel if enabling and protective government regulations are established.

General

Supervisory control communicates orders from a dispatcher to equipment at a remote station, which in turn acknowledges these orders and verifies their execution.

The interconnecting means between the dispatcher and remote stations is of the utmost importance to a supervisory control system, just as it is to any communication system. The interconnecting means for transmitting intelligence between two or more locations for any communication service constitutes a channel.

While supervisory control may employ any conceivable type of channel, in practice it is found that a simple system of keying electric potential into pulses is the most acceptable. A system of this type uses a simple transmitting relay and a simple receiving relay at each station. In the basic arrangement of this system, d-c operation over two metallic conductors is employed.

Where the metallic circuit is used for other services besides supervisory control, then the system used must provide non-interfering channels over the metallic circuit. Such noninterfering channels can be established by using d-c impulses for supervisory control with a-c signals for the other services. Where the metallic conductors are power lines and the major service is the transmission of power frequency, d-c impulses for supervisory control are impractical. In these cases, the supervisory control signals are transmitted as a-c pulses at a power-line carrier frequency.

An important extension of the idea of pulsing alternating voltage, is the use of space radio as a channel between the supervisory control stations.

A channel consists of a transmission medium and terminal equipment. When operation is by means of d-c impulses over a metallic circuit, the transmission medium is the pair of wires and the channel terminal equipment consists solely of protective equipment. When operation is by means of a carrier frequency on a power line, the transmission medium is the power line and the channel terminal equipment consists of the carrier frequency generating and receiving equipment as well as the necessary components for introducing the carrier frequencies to the transmission line. When operation is by means of space radio equipment, the transmission medium is the ether and the channel terminal equipment consists of the radio frequency generating and receiving equipment as well as the necessary antennas. Figure 1 indicates the transmission medium and the channel terminal equipment required to establish a supervisory control channel on a pair of telephone wires, on a power line, and through space.

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W. A. Derr and M. J. Brown are both with the switchboard engineering department of the Westinghouse Electric Corporation, East Pittsburgh, Pa.

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Supervisory control terminal equipment is essentially the same regardless of the type of channel employed. Thus, in choosing a channel means for operation of supervisory control equipment, the choice should be governed primarily by a comparison of the possible channel means themselves. The following factors should be taken into account in determining the most suitable channel means for a given application.

1. Reliability.
2. Simplicity.
3. Cost of installation.
4. Cost of maintenance.
5. Privacy.
6. Freedom from interference.
7. Freedom from legal regulations.

Telephone Line Wires

More than 95 per cent of all of the existing installations of supervisory control equipment operate over telephone-line wires. These installations operate over open construction telephone lines or wires in telephone cables, some of which are leased from telephone companies and some of which are privately owned. Supervisory control equipment as manufactured today operates over a single pair of conductors.

Privately Owned Cable Wires

When it can be economically justified, the use of a twisted pair of cable wires for the transmission of d-c supervisory control impulses is recommended. The use of a pair of cable wires can often be justified if other pairs in the cable can be assigned to other functions such as pilot wire relaying. The use of d-c impulses minimizes interference with other services and places no practical limitation on the length of cable over which control can be provided. An installation is in operation over 125 miles of number 16 gauge cable wires. Lead-sheathed cable either buried in the ground or whose sheath is frequently grounded is preferred since it inherently has a high degree of protection against lightning and electrostatic induction.

Cable wires are subject to the following disturbing voltages:

1. Induction from power lines.
2. Lightning and other surges.
3. Elevation of station ground potential.

When a twisted pair of cable wires is employed, no appreciable voltage will appear between the wires for any of the disturbing voltages listed. Thus, the only thing which must be considered is the voltage to ground which can appear on both wires.

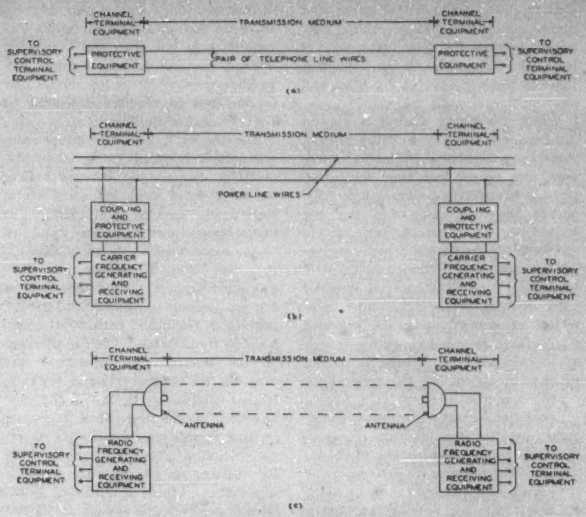


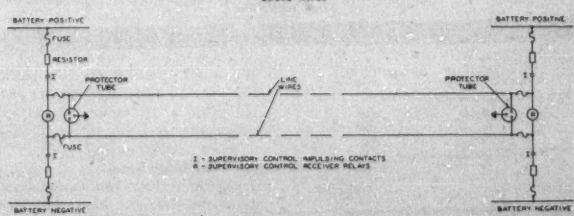
Figure 1. Diagram indicating channel transmission medium and channel terminal equipment for supervisory control operation

(a) telephone line wires, (b) power-line carrier, (c) space radio

On the usual installation of supervisory control equipment, voltage will only appear on the cable wires momentarily for any of these disturbing voltage conditions, since there will only be an appreciable amount of induced voltage and a difference in ground potential during adjacent or nearby transmission line faults. Thus, the duration of the disturbing voltage will never be greater than several seconds even when slow-speed relaying is involved on the transmission lines. There is usually no objection to having the supervisory control channel momentarily inoperative during power-line fault conditions. Hence, protection of the supervisory control equipment at each station is normally provided by a gas-filled protector tube which will break down between 350 and 550 volts rms and which will discharge currents of the order of 50 amperes for two seconds.[3] The protector tubes are connected as shown in Figure 2. The protector tube has an average arc drop of 20 to 30 volts of direct current. The supervisory control circuits are arranged so that the arc is cleared and the line returned to normal as soon as the disturbing voltage disappears.

When protector tubes are applied to cable wires, they should be connected to all wires in the cable which are used for any service and the unused con-

Figure 2. Usual protection for privately owned cable wires



ductors should be grounded unless it is known that the voltage which may appear between the wires and ground is less than the rating of the insulation between wires or between the wires and the sheath.

When supervisory control is applied for the control of a-c electrified railway substations, additional protective equipment may be required, since the single-phase ground-return system induces voltage in the cable wires from the flow of load current, under conditions of stub end feed. If the control cable is in close enough proximity to the catenary wires to induce voltages of several hundred volts or more in the cable wires, protective equipment should be provided to distribute the voltage in such a manner as to maintain the voltage to ground which can appear on the supervisory control equipment to a low value.

Simple resistor-type drainage equipment can be used in some cases, as shown in Figure 3 (a). The resistance values of the resistors are determined by the maximum induced voltage which can result from load current, the number of stations being operated on a single pair of wires, and the resistance of the wires. The induced voltage is given a path to ground through the resistors, the voltage thus being dissipated as resistance drops. The value of the resistors connected from the wires to ground must be such that the amount of voltage which can appear between the wires and ground for maximum load conditions is less than the breakdown voltage of the protector tube. The padding resistors in the line circuit may or may not be required depending on the resistance of the wires.

The use of a 2-winding neutralizing transformer is usually preferred for distributing continuously induced voltage on cable wires used for operation of supervisory control equipment.[3] The neutralizing transformers are connected to the cable wires as shown in Figure 3 (b). The two microfarad capacitors connected from the equipment side of the neutralizing transformers to ground provide an excitation path for the neutralizing transformer. The two windings of the neutralizing transformer are wound in the same direction on the core, so that currents which flow in the same direction in the line wires encounter exciting impedance, whereas the supervisory control impulses encounter only the leakage impedance. The exciting impedance of these neutralizing transformers can be made as high as 100,000 ohms at 60 cycles with only 160 ohms added to the loop resistance of the wires. Two microfarad grounding capacitors as indicated in Figure 3 (b), have a parallel impedance of 663 ohms at 60 cycles and limit the voltage on the supervisory control equipment to about 0.66 per cent of one-half of the induced voltage, assuming that one-half of the voltage is impressed across each neutralizing transformer. Thus, the high voltage appears from the line side of the neutralizing transformers to ground and a low voltage appears on the equipment side of the neutralizing transformers to ground.

When neutralizing transformers are applied to a pair of cable wires, lightning arresters may be required on the line side of the neutralizing transformers if the wires can be subjected to disturbing voltages of a higher magnitude than the maximum induced voltage for which the circuit must remain operative. The lightning arresters must be coordinated with the insulation rating of the cable. Also, if neutralizing transformers are not applied to all of the wires in a cable, the insulation between wires of the cable must be sufficient to withstand the voltage difference which can appear between the wires with neutralizing transformers and the wires without neutralizing transformers.

Privately Owned Open Construction Telephone Lines

Open construction telephone lines can be subjected to the following disturbing voltages:

1. Induction from power lines.
2. Lightning and other surges.
3. Elevation of station ground potential.
4. Direct crosses with high-voltage lines.

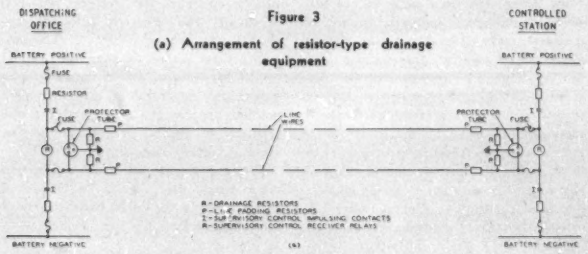
Where possible, it is desirable to have the telephone line located remote from any transmission line to eliminate the possibility of direct crosses with high-voltage lines and to reduce the magnitude of induced voltage. With open construction lines, there is the possibility of induced voltage between the line wires as well as of induced voltage to ground. The telephone wires must be transposed at frequent intervals to keep to a minimum the induced voltage between line wires resulting from load current in nearby transmission lines. Close spacing of the telephone wires also helps keep the voltage between wires to a low value.

If the telephone line wires are located so that there is no possibility of contact with high-tension lines, and if the induced voltage which can appear to ground is low for transmission line load currents, protection can be provided by a gas filled protector tube at each supervisory control location as described for the protection of cable wires. Lightning arresters can also be used as back-up protection for the protector tubes.

Since the control lines usually run

Figure 3

(a) Arrangement of resistor-type drainage equipment



(b) Arrangement of 2-winding-type neutralizing transformer

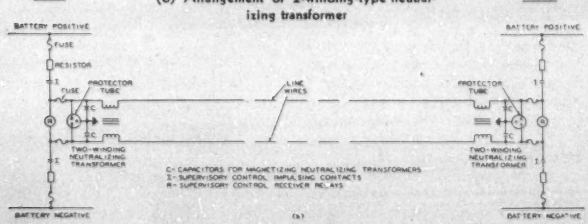
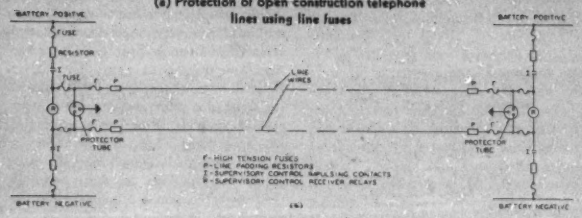
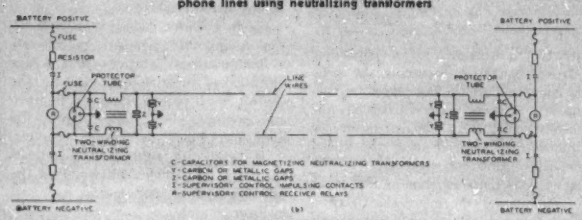


Figure 4

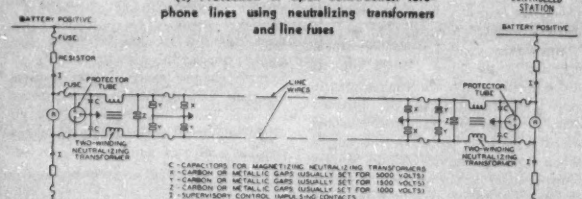
(a) Protection of open construction telephone lines using line fuses



(b) Protection of open construction telephone lines using neutralizing transformers



(c) Protection of open construction telephone lines using neutralizing transformers and line fuses



between power stations, economics often dictate they be strung on the same pole. To protect the equipment and personnel when there is a possibility of direct contact with a power line, additional protective equipment should be used. Minimum additional protective equipment can be added if permanent interruption of the supervisory control circuit is warranted for a direct contact with a high-tension line. The additional minimum protective equipment in this case consists of high-tension fuses which interrupt the line circuit on the current which flows to ground through the protector tube when there is a contact with a power line. The rating of the fuse should be such that the fuse will not blow on the current which can momentarily flow as a result of induced voltage or difference in ground potential. Padding resistors are sometimes necessary in the line circuit to obtain discrimination between the current which can flow for a direct contact with a power line and the current which can flow as a result of induced voltage or difference in ground potential. The arrangement is shown in Figure 4 (a).

Figure 4 (b) shows protective equipment for open construction line wires strung on the same poles as high-tension lines, which provides the same degree of protection for personnel and equipment as could be provided if an insulating transformer were to be used instead of a neutralizing transformer. The use of a neutralizing transformer permits operation by d-c impulses.

With the protective arrangement shown in Figure 4 (b), the voltage which can appear on the equipment side of the neutralizing transformer is limited by the high impedance of the neutralizing transformer to other than circulating current, with respect to the impedance of the drainage capacitors. The protector tube on the equipment side of the neutralizing transformer limits the voltage on the equipment to between 350 and 550 volts rms in the case of direct contact with high-tension lines. For any disturbing voltage condition, a high voltage is built up on the line side of the neutralizing transformer. If this voltage is high enough, the gaps on the line side of the neutralizing transformer break down. When only one winding of the neutralizing transformer has voltage impressed on it, it presents a high impedance. Thus, in the event of contact of one of the supervisory control line wires with a power line, a high voltage immediately appears to ground from the line side of the neutralizing transformer winding in the wire contacted. The gap to ground, shown in Figure 4 (b), breaks down, with the current flow to ground only being limited by the resistance of the line wire. The gaps on the line side of the neutralizing transformer should have a rating which will carry the flow of current which results from a direct contact with a power line for a considerable length of time. Normally, the time which this current flows is not more than several seconds due to the opening of circuit breakers on the power line.

If it is desired to provide the maximum protection including a fuse to interrupt the line circuit in the event of a direct contact with a high tension line, an arrangement as shown in Figure 4 (c) can be used. With this arrangement, the fuse rating must be such that it will interrupt the circuit on the current which will flow through the gaps between the neutralizing transformer and the fuses, when there is a direct contact with a high tension line. When the fuse interrupts the circuit, the high voltage is transferred to the gap on the line side of the fuse. This gap then breaks down and effectively grounds the line wire for as long as the power-line voltage remains on the supervisory control-line wire.

The gaps shown in Figures 4 (b)

Table I

Frequency, Cycles	Maximum Voltage
300	30
500	8
1,000	1
1,500	1
2,000	0.75
3,000	0.40
4,000	0.30
5,000	0.25

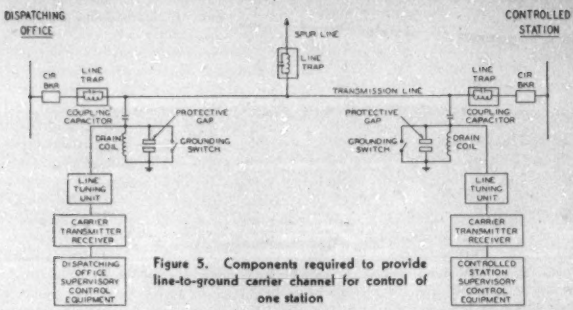


Figure 5. Components required to provide line-to-ground carrier channel for control of one station

and 4 (c) can be either carbon or metallic. There is an advantage in the use of carbon gaps, since there is no tendency to weld and thus permanently short circuit the line to ground, or cause a short circuit between wires.

Leased Telephone Channels

A large number of supervisory control installations are operated over channels leased from telephone companies. The channel leased from a telephone company may consist of a cable pair, a pair of open construction wires, a phantom circuit, or a combination of these types of circuits. Protective equipment similar to that provided for commercial telephone circuits is ordinarily provided for leased channels by the telephone companies.

Supervisory control equipment which operates by means of d-c coded impulses, is designed to be well within the voltage and current limitations imposed by the telephone companies. A maximum of 135 volts of direct current to ground and 270 volts of direct current between the telephone wires may be used. The maximum permissible current is 0.35 ampere.

When audio frequencies are employed for continuous telemetering in conjunction with supervisory control, the voltage limitations shown in Table I are usually imposed. The voltage values may have to be substantially reduced in cases where the circuit or terminal equipment is unbalanced to ground.

Power-Line Carrier Channels

In operating supervisory control equipment over a power-line carrier channel, the carrier transmitters are keyed to transmit the coded impulses and carrier receiver relays pass along the impulses to the supervisory control equipment itself. The supervisory control terminal equipment for operation on a power-line carrier channel is essentially the same as equipment which is employed on telephone-line wires. The carrier channel merely constitutes a different means for transmitting and receiving the coded impulses. However, where selective telemetering is required, some of the types of telemetering equipment which can be used in conjunction with supervisory control equipment operated over telephone-line wires, cannot be used on carrier channels.

Supervisory control equipment which operates by means of a single type of coded impulse and hence requires a single receiving element, can be operated over the same type of carrier channel which is used for protective relaying.[4,5] When no other service besides supervisory control is provided on a carrier channel, the transmitters and receivers at all stations are operated on the same frequency and no modulation by audio tone equipment is required.

Carrier channels can be employed over relatively long distances providing the losses are not too great and the noise level is not too high.[6] Therefore, a carrier channel can often be advantageously employed where long distances are involved. One supervisory control system has been in operation for more than ten years on a power-line carrier channel over a distance of 270 miles.[7,8]

Carrier on a power line provides a very reliable channel. However, since carrier for supervisory control operation is usually operated from one phase-to-ground, a ground on the phase to which the carrier is coupled may make the equipment inoperative. In some instances, greater reliability can be obtained by providing alternate routes for the carrier signals.

Figure 5 shows in diagrammatic form the components required to provide a line-to-ground carrier channel for operation of supervisory control for the control of a remote station. If there are any spur lines tapped from the lines over which the carrier operates, it is usually desirable and sometimes necessary to provide line traps at the junction of these spur lines as indicated in Figure 5.

Up until fairly recently, power line carrier channels were limited to the 50- to 150-kc range. This range has now been extended to 300 kc which has helped considerably in the finding of suitable channel space for the various functions for which power-line carrier has been employed. Also, the use of single side-band carrier channels makes possible a greater number of noninter-

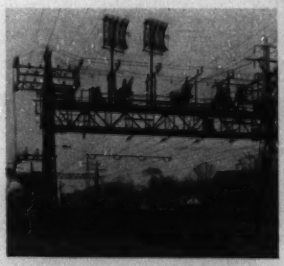


Figure 6. Micro-wave antennas at Darien for tests on New York, New Haven and Hartford Railroad Company property

Figure 7. View of micro-wave transmitter receiver cabinet used in tests on property of New York, New Haven and Hartford Railroad Company

fering channels.[9] However, in some locations where large numbers of carrier channels have been installed, the choice of suitable additional channels is still a problem, since it is difficult to limit a carrier frequency to a desired portion of an interconnected system.

Supervisory control can sometimes be combined with other functions for operation on the same carrier channel.[10] This sometimes requires that the supervisory control equipment be operated on a single audio tone modulating the carrier frequency.[11] When such combination can be made a saving in cost results and there may also be a reduction in the total number of different carrier frequencies required. However, there are numerous problems in making such combined applications and each case usually requires individual study.

There are some instances in which the power line is not available at the desired location for the dispatching office end of a supervisory control system. In these instances the channel must be extended from the carrier channel to the dispatching office by means of telephone-line wires. This necessitates delivering the supervisory impulses from the carrier channel to the telephone wires and vice versa. The provisions for delivering the impulses and the requirements of the telephone-line wire circuit are dependent on whether a single or a 2-frequency carrier channel is used, on the length of the telephone wire circuit, and on whether other functions besides supervisory control are provided on the carrier channel.

Space Radio Channels

Consideration has been given for a long period of time to the possibility of using space radio channels for the operation of supervisory control equipment. Development of micro-wave equipment with its directional and thus less interfering feature has opened the door to the possibility of using space radio channels for the operation of supervisory control if legal conditions permit. The first field tests to determine the feasibility of using micro-wave radio channels for operation of supervisory control equipment were made in 1943. An installation of coded d-c impulse-type supervisory control equipment has been in operation over a pair of cable wires between the Cos Cob power plant and the Darien substation of the New York, New Haven and Hartford Railroad since 1939. The two stations are 6.25 air line miles apart. For these tests, the supervisory control terminal equipment was disconnected from the line wires and temporarily connected to micro-wave radio transmitter-receiver equipment for a period of a month.

Two antennas were mounted at each station, one for transmitting and one for receiving. Each antenna consisted of four full-wave dipoles. At the Cos Cob power plant, the antennas were placed on the roof of a boiler house, approximately 90 feet above mean sea level. At Darien substation, the antennas were mounted on wooden poles on top of an anchor-bridge approximately 108 feet above mean sea level. Figure 6 shows the two antennas mounted at Darien substation. Although the terrain did not permit "line-of-sight" transmission, satisfactory operation was obtained.

At Cos Cob, the transmitter-receiver cabinet was mounted adjacent to the antenna on the roof, with the keying, receiver, and power leads carried to the supervisory control equipment in the control tower some 300 feet away. At Darien, the transmitter-receiver cabinet was located in the substation building and connected to the antennas through approximately 75 feet of coaxial cable. Figure 7 shows a view of the transmitter-receiver cabinet used at each location.

A frequency of approximately 525 megacycles was used. Operation was tried by keying the micro-wave frequency direct and by keying a modulating audio frequency. Both methods of operation were satisfactory.

Supervisory control equipment has been manufactured for the United States Coast Guard for the control of an unattended lightship from the shore, by means of space radio equipment. The supervisory control equipment will be used to start and stop Diesel generators, operate beacon signals, and so forth.

A separate radio frequency will be used for the transmission of supervisory control impulses in each direction. A frequency of 165.375 megacycles will be used to transmit impulses from the shore to the lightship and a frequency of 171.375 megacycles will be used to transmit impulses from the lightship to the dispatching office located on the shore. The supervisory control equipment will operate by keying tone equipment which will frequency modulate the radio transmitter output.

Figure 8 shows the lightship which will be controlled. A towing vessel is partly visible behind the lightship.

The Federal Communication Commission has not as yet allocated any micro-wave radio bands for operation of supervisory control equipment on a permanent basis. However, the band from 952 to 960 megacycles as well as other bands up to frequencies as high as 6,000 megacycles are now available for experimental installations. If experimental usage is made there is a possibility that these bands may be allocated on a permanent basis.

The band from 952 to 960 megacycles is particularly appealing since conventional tubes and circuits can be used in manufacturing equipment for this band of frequencies.

Conclusions

Supervisory control terminal equipment which operates by coding a single type of impulse can be fundamentally the same regardless of the channel employed. Therefore, the reliability of such a supervisory control system is largely determined by the reliability of the channel transmission medium and the channel terminal equipment. Telephone-line wires and power-line carrier channels have been accepted through the years as reliable channel means.

Micro-wave radio equipment for operation of supervisory control equipment may be made commercially available in the near future. There is every reason to believe that this will be as reliable as the channel means previously used.

If more than one reliable channel is available for a given application, the

Figure 8. View of United States Coast Guard light ship which will be controlled by supervisory control equipment operating over space radio channel

choice is governed by the cost of the channel terminal equipment plus the cost of the channel transmission medium. The expected cost of replacement parts and cost of maintenance should also be considered.

Legal restrictions, privacy and freedom from interference are other factors which warrant consideration.

The next several years will prove interesting as regards the use of microwave channels for the transmission of supervisory control impulses, since this period will probably determine whether or not this channel means comes into as general usage as telephone-line wires and power-line carrier channels. Experience will indicate the limitations of operation without repeat stations as governed by the intervening terrain.

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No Discussion

Short Time Ratings of Diesel-Electric Locomotives

D. R. CAMPBELL
NONMEMBER AIEE

T. J. WOODS
ASSOCIATE AIEE

IN THE PAST quarter century, the Diesel-powered locomotive has developed into one of the most efficient and economical forms of motive power available to railway and industrial users. It is a versatile unit, performing equally well in all types of service, ranging from 100 horsepower lightweight units used in intra-plant switching to huge 8,000 horsepower locomotives for mainline freight service.

This versatility is due largely to the d-c electric transmission system interposed between the prime mover and the driving wheels. The Diesel engine is a relatively constant torque machine having little torque variation between idling and full speed. On the other hand, the torque required at the driving wheels of a locomotive varies from the maximum required at starting to low values at high speed, corresponding to the available engine horsepower.

The transmission system consisting of generators, series type motors, and control, provides an effective means of changing the constant torque of the prime mover to the everchanging values of torque required at the rims of the driving wheels. To date, no other method of power transmission, and torque conversion can equal the electric system in reliability, economy of maintenance, and effectiveness.

The use of the Diesel-electric locomotive brought with it the problem of determining the maximum trailing tonnage the unit is capable of pulling over a given section of a railroad. With the reciprocating steam locomotive, this is a simple problem. The steam locomotive can take the maximum load it is capable of starting and hauling over a given profile without damage to its component parts. Too great a load will result in stalling on the ruling grade, or inability to start the train, however, no harm will result. Where running time is the determining factor, the train will be loaded with the number of cars it is capable of hauling to its destination within the scheduled time.

In the case of the Diesel-electric locomotive, the problem is more difficult. The ability of the electric transmission system to take loads in excess of the continuous rating invites the possibility of dangerously overloading the electric propulsion equipment in order to move a desired tonnage. The continuous rating of a Diesel-electric locomotive is the tractive force developed at the rail, expressed in pounds, for any period of time without the temperature rise of the generator or motors exceeding 120 degrees centigrade on the armature, or 130 degrees centigrade on the fields, as measured by resistance. This continuous rating, however, gives little indication of the tonnage which can be handled on short sections of heavy grade; therefore, various methods have been devised to determine the maximum tonnages which may be handled for short times, thus utilizing the electric equipment to its fullest capacity. The ultimate of electric loading would be an everchanging load to compensate for the changing grade, track conditions, and so forth, to give a constant temperature at the maximum rated temperature rise.

While the maximum motor current is limited by the maximum adhesion between the driving wheels and rail which may vary from 10 to about 35 per cent, depending on track conditions, the continuous current is limited by the temperature rise of the electric equipment. The temperature of traction motors is difficult to measure in service as it means stopping the locomotive at some point and applying temperature measuring devices. The readings then have to be extrapolated to determine the cooling that took place between power cutoff and the time the first temperature reading was taken. This method is unacceptable in normal service and is restricted to special testing of locomotives. Therefore, it is of little practical value in determining locomotive ratings. To date, there is no practical instrument to indicate the temperature of the electric equipment to the locomotive engineer. Even such an instrument would not completely solve the problem. It is desirable to know at the starting point what tonnage the locomotive is capable of hauling over the profile. An accurate temperature indicating device will inform the operator when to reduce the load on the electric apparatus. However, at this point, a stop may be impractical from the railroad's point of view.

There are, in general, two types of service performed by locomotives in railway work. These are classified as switching and road service. They may be further classified as yard, terminal, and hump switching, road transfer, road freight, and passenger service. Because of the difference in the duty cycle for road and switching work, a different method is used in each case to determine the capability of the locomotive in that particular service. The duty cycle imposed on the electric equipment in normal switching service is not usually of a severe nature, especially in industrial work. It usually consists of accelerating a string of cars to about ten miles per hour, coasting a short distance, then braking to a stop. Thus, current is flowing through the traction motors for only a

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D. R. Campbell and T. J. Woods are both with the Westinghouse Electric Corporation, East Pittsburgh, Pa.

Table 1. Tonnage Rating for 80-Ton Diesel-Electric Locomotive

600 Horsepower Net Input to Generator
8 Pounds per Ton Train Resistance
Tangent Track

Per Cent Ruling Grade	1/2 Mile Train Movement	1 Mile Train Movement	3 Mile Train Movement	Continuous Train Movement
0	3,610*	3,610*	3,220	2,680
0.1	3,110*	3,110*	2,560	2,130
0.2	2,740*	2,620	2,120	1,760
0.3	2,450*	2,240	1,810	1,500
0.4	2,200*	1,940	1,570	1,300
0.5	2,000*	1,720	1,390	1,148
0.6	1,810	1,540	1,240	1,025
0.7	1,640	1,392	1,120	925
0.8	1,496	1,270	1,020	840
0.9	1,375	1,170	936	770
1.0	1,270	1,080	864	710
1.1	1,180	1,000	800	656
1.2	1,100	930	745	610
1.3	1,030	874	696	570
1.4	970	820	654	534
1.5	915	773	615	501
1.6	865	730	580	472
1.7	820	691	548	445
1.8	780	656	520	422
1.9	742	625	494	400
2.0	708	595	470	380
2.2	648	543	428	345
2.4	596	498	392	314
2.6	550	460	360	288
2.8	512	426	333	265
3.0	476	396	308	244

* Limited by 30 per cent adhesion; 5 pounds per ton for acceleration.

These trailing loads are based on the power plant operating at full speed and horsepower. These tonnages cannot be hauled if the locomotive has been operating up to continuous temperature limits. A 15-minute cooling period must be interposed before the overload operation is started if the equipment has been operating up to AIEE temperature limits.

(Figures shown are Tons Trailing Load)

short time. Experience has shown that a continuous tractive force rating of approximately 13 to 15 per cent of the weight on drivers is sufficient to handle typical switching operations without danger of overheating the electric equipment. Tonnage charts may be prepared for switching service for runs of different lengths and on various grades. A typical tonnage chart is shown in Table I. This covers a 650-horsepower 80-ton locomotive and shows the length of run and permissible trailing tonnage for a range of ruling grades. The tonnage charts are based on a 15-minute cooling period from the maximum temperature rise preceding the application of the overload. Table I was derived from the curves of Figure 1. The tractive force-time curve is plotted from data obtained from motor tests and the tractive force-speed curve is part of the combined performance curve, Figure 2. The distance curve, thus, shows the tractive force which can be maintained for a given distance. From this the trailing tonnage, which can be hauled for a given distance after the traction equipment has

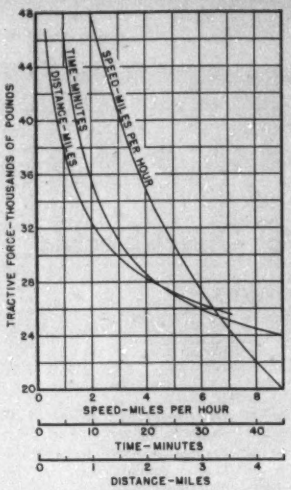

Figure 1. Curves for determination of tonnage tabulation

cooled for 15 minutes, is calculated by the following equation:

$$W_t = \frac{TF}{TR + 20G} - 80$$

where

W_t = weight of trailing train, in tons.
TF = tractive force, in pounds, available for a fixed distance.
G = grade, in per cent.
TR = train resistance, in pounds per ton. This varies with the weight, number of wheels, and so forth, of each car. Eight pounds per ton has been assumed as an average, for the entire train.

Road service is vastly different from

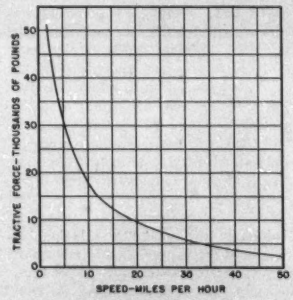

Figure 2. Combined performance curve of 650 horsepower locomotive

switching service as the Diesel engines may be loaded to maximum output for acceleration and held at full rated load for a considerable length of time. This service imposes a more severe duty on the Diesel engine and electric equipment than normal switching service. With a continuous tractive force rating of 13 to 15 per cent of the weight on drivers for passenger service, and 18 to 20 per cent for freight service, the electrical maintenance should hold at a reasonable level. As road locomotives usually operate on varying profiles, the tonnage rating may be determined by use of heating and cooling curves of traction generators and motors. This is done by assuming a tonnage somewhat greater than can be handled at the continuous rating on the ruling grade, and then calculating the temperature rise of the propulsion equipment in pulling the train over this peak grade.

As an example, assume a 1,500-horsepower locomotive weighing 125 tons operating on a rolling profile such as shown in Figure 3 with a trailing load of 2,500 tons. At point "A", the equipment is assumed to be 40 degrees above the ambient after doing general switching work prior to leaving the terminal. The tractive force required to handle the train on the grade section from "A" to "B" is calculated as follows:

Grade resistance = 0.1 × 20 pounds per ton = 2 pounds per ton
Train resistance = 5.7 pounds per ton (Davis train resistance of 40-ton car at 23 miles per hour)
Total tractive force required = (2 + 5.7) × (2,500 + 125) = 20,200 pounds

From the performance curve of this locomotive, Figure 4, a tractive force of 20,200 pounds is developed at 23.3 miles per hour and at 79.4 per cent current on FS-2. At this speed, neglecting acceleration, the locomotive will reach point "B" in 25.8 minutes. The heating and cooling curve, Figure 5, shows 9.5 minutes as the intersection of the 40 degree rise ordinate and the 79.4 per cent current curve, therefore, the time at "B" is 9.5 + 25.8 = 35.3 minute abscissa and the 79.4 per cent current curve. The resulting temperature rise will be 81 degrees. The tractive force

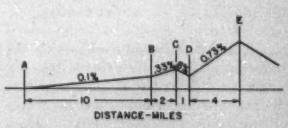

Figure 3. Typical railroad profile

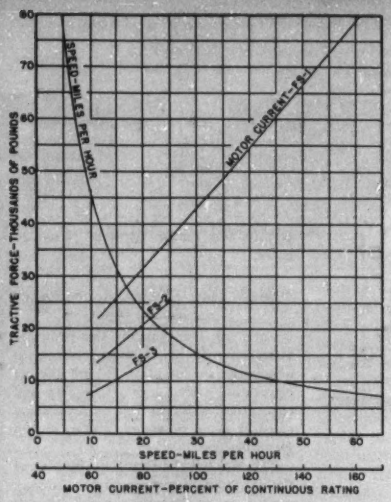

Figure 4. Combined performance curve 1,500 horsepower locomotive

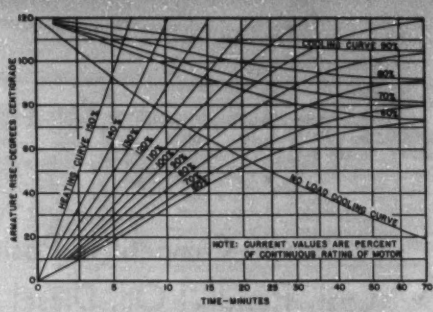

Figure 5. Heating and cooling curves for electric equipment on 1,500 horsepower Diesel-electric locomotive

required from "B" to "C," neglecting deceleration is:

Grade resistance $= 0.33 \times 20 = 6.6$ pounds per ton

Train resistance $= 5.2$ pounds per ton (Davis train resistance of 40-ton car at 16 miles per hour)

Total tractive force required $= (6.6 + 5.2) \times (2{,}500 + 125) = 31{,}000$ pounds

From Figure 4, 31,000 pounds tractive force is developed at 15.2 miles per hour and 79.5 per cent current on FS-1. At 15.2 miles per hour, the time from "B" to "C" is 7.9 minutes. Starting at 81 degrees, Figure 5, 7.9 minutes on 79.5 per cent current will increase the temperature rise to 85 degrees. On section "C" to "D," no power is required; and neglecting acceleration, assume train runs at the speed limit of 40 miles per hour for 1.5 minutes. Again, refer to Figure 5 and the no load cooling curve which shows 1.5 minutes cooling reduces the temperature to 80 degrees rise. The tractive force required from "D" to "E," neglecting deceleration, is:

Grade resistance $= 0.73 \times 20 = 14.6$ pounds per ton

Train resistance $= 4.8$ pounds per ton (Davis train resistance of 40-ton car about 9 miles per hour)

Total tractive force required $= (14.6 + 4.8) \times (2{,}500 + 125) = 51{,}000$ pounds

From Figure 4, 51,000 pounds tractive force is developed at 8.8 miles per hour and 114.5 per cent current on FS-1. At 8.8 miles per hour, the time from "C" to "D" is 27.3 minutes. Since the 114.5 per cent current line starting at 80 degrees crosses the 120 degrees temperature rise in 14 minutes, in place of the 27.3 minutes required, the load must be reduced to protect the electric equipment from early failure. The same calculations must be made again to check the temperature rise with a smaller load. It will be found that a tonnage rating of approximately 2,200 tons will apply on this profile. From the foregoing, the heating and cooling curve method is evidently a cut and dry proposition but gives results sufficiently accurate for general application studies.

It is to be noted that momentum, acceleration, and deceleration were not considered in the preceding example. In going from a negative to a positive grade, the train will gradually slow down until a balancing speed is reached after which a constant velocity will be maintained up the grade. Neglecting momentum and using balancing speeds will result in a slightly pessimistic answer; because of the many variables involved, this is desirable.

Under certain conditions, it may be found necessary to consider momentum in negotiating short sections of heavy grades, in which case a continuous speed-time curve will serve to determine the limiting tonnages.

A third method of calculating the severity of a service is the rms method as used in industrial applications. This serves well where the duty cycle is of short duration and is of a repetitious nature. However, in a transportation service where the locomotive may be subjected to an overload on one severe grade until the maximum temperature rise is reached, the rms method does not give an accurate indication of the severity of the service and is, therefore, not of great value in calculating locomotive tonnage ratings in mainline railway service.

Although Diesel-electric locomotives have now been in general use for almost a quarter of a century, no standard methods have been adopted to determine the hauling capacity of a Diesel-electric locomotive for the various services it may perform. It is recognized that the thermal capacity inherent in electric apparatus greatly increases the value of the electrically-propelled vehicle, and acceptable methods of evaluating this characteristic should be available for general use. The methods presented herein are offered as a possible solution for determining tonnage hauling capacities or as a foundation on which to base a practical system acceptable to all concerned.

No Discussion

The Present Competitive Position of Coal, Residual Oil, Diesel Oil, and Electricity as Energy Sources for Motive Power Operation in the Pacific Northwest

T. M. C. MARTIN
ASSOCIATE AIEE

IN the Pacific northwest as in other regions, the Diesel-electric locomotive has become an important factor in main-line railway operations. Before very long it is probable that 50 per cent of all movements will be handled with Diesel-electric motive power. This trend focuses attention upon the relative costs of supplying main-line services with different types of motive power, and particularly upon the most important single element of differential cost, that is, the cost of fuel. The fuel utilized in Diesel-electric locomotives comes from the same fraction of the total petroleum barrel which also is the source of the distillate fuels in heavy demand for highway, farm, residential space heating, and other purposes. Jet-propelled aircraft likewise depend upon middle distillate fuels, a factor of some consequence if another war should transpire. Lately the price of Diesel fuel has begun to reflect these competing uses, although it has not as yet reached a level that exhausts its potentialities for price adjustment, especially in view of the level which residual oil has attained.

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T. M. C. MARTIN is with the Bonneville Power Administration, Portland, Oreg.

There is substantial doubt that further dieselization can be accomplished in the Pacific northwest without paying an excessive premium for the presumed advantages of self-contained motive power units. Based on mid-1948 costs, there appear to be sound reasons for railway managements giving full and immediate consideration to the use of the hydroelectric energy with which the region is endowed. By dieselizing yard and branch-line services, and electrifying main-line operations, these railways can achieve a measure of operating cost stability in an otherwise doubtful situation. This step would require the investment of some new capital, both in fixed plant and rolling stock. Analysis of the available evidence on as objective a basis as possible indicates that such new capital investment could be recovered by operating savings in almost record time.

It is concluded, for example, that an investment of about $6,000,000 to change from Diesel-electric to straight-electric operation on a typical 400-mile division in this region, would produce annual operating savings of about $1,600,000, equal to a little over 25 per cent interest on the new capital invested. This is equivalent to saying that the management is not required to foresee or have faith in the future for more than four years. This fact alone should offer some encouragement in a world that currently permits few longer vistas of things to come.

Rise of Diesel-Electric Motive Power

Early in 1948 one of the principal manufacturers of railway locomotives announced its intention to quit the reciprocating steam locomotive field and concentrate all its effort on the production of other types of motive power, chiefly Diesel electric.

This was but the formal recognition of the existence of a revolution in railway motive power, something which, though it has been denied by many authorities, has been pretty well established by the figures on new locomotives ordered in recent years. Railway Age* has reported a total of 3,064 orders for new Diesel-electric locomotives (domestic use only) in the past two years against but 134 steam locomotives. Purchases of new steam switching units virtually ceased several years ago and the business of steam locomotive building as of today has declined to the point that remaining production is to fill a few "token" orders placed by the so-called "coal roads."

Although purchases of new steam locomotives have declined almost to the vanishing point, there are large numbers of various classes still in service and it is probable that railway motive power rosters will include a substantial representation of steam power for a long time to come. Any realistic view of motive power trends must recognize, however, the significant inroads that Diesel-electric operations have made in comparatively few years in a field dominated for over a century by reciprocating steam power. Interstate Commerce Commission statistics reveal that as of May 1, 1948, there were about 5,000 Diesel-electric locomotives in service on Class I carriers. The distribution by types is given in Table I.

Mere numerical strength is not the best index of the relative importance of Diesel-electric motive power from an operational standpoint. Although on May 1, 1948, Diesel-electric units constituted but 13

* Published weekly in New York.

Table I. Distribution of Locomotives on Class I Railways in the United States as of May, 1948

Classified by Assigned Type of Service[1]

Assigned Service	Total	Type of Locomotive: Steam	Other	Diesel-Electric	Per Cent Diesel-Electric
Yard switching	13,250	9,894	185	3,180	24
Road freight	21,132	19,523	316	1,293	6
Road passenger	6,124	5,084	293	747	12
Total	40,515	34,501	794	5,220	13

1. For all numbered references, see list at end of paper.

per cent of locomotives in service, they were accounting for a much higher proportion of the total work units being performed. Table II shows the relative work load carried by the total Diesel-electric roster during the first four months of this year as reported by the Interstate Commerce Commission. Table III relates the work performance to the numbers of Diesel-electric locomotives in use as of the close of the period, and shows that Diesel motive power is accounting for from 150 to 300 per cent of the work units that would be expected of them purely on the basis of their numerical strength. Part of this splendid performance can be explained on the basis of the relative ages of the locomotives in service but in a very substantial degree the better than average performance of the Diesels is attributable to the superiority of their electric-traction-motor drives.

It can be seen that with better than 2,000 new Diesel-electric locomotives scheduled to be added to rosters this year and with no evidence of any slackening of orders, the time cannot be far distant when 50 per cent of the total work units will be performed by Diesel-electric motive power. It is not the purpose of this paper to discuss in detail the reasons why Diesel-electric units are causing so much absolescence in steam motive power. Previous papers have developed the basic facts regarding the technological changes that are responsible for this trend, and it seems necessary to mention briefly only the cardinal points. In yard switching service, Diesel-electric locomotives possess two important physical advantages:

1. Because all weight is normally motorized they are able to produce higher starting and continuous tractive effort than comparable steam units and therefore they merit larger tonnage ratings.

2. With no fires to clean, ashes to dump, boilers to wash, and with less lost time for assorted other reasons, they have demonstrated a high degree of availability and reliability.

In road passenger service their higher starting and continuous tractive effort, lower center of gravity, freedom from dynamic augment, plus their ability to negotiate long distances between stops for fuel and water, have combined to make them ideally suited to the so-called "streamliner" service. As a road freight locomotive the Diesel-electric excels for a reason that is sometimes overlooked by its most ardent advocates. Largely because of its electrical transmission it is able to develop its full horsepower output at comparatively low speeds and thereby create relatively high continuous tractive efforts at these same low speeds. High continuous tractive effort is in turn the basis for substantial tonnage ratings. And, since all weight is usually on drivers, adhesion is seldom a limiting factor in converting tractive effort into drawbar pull. The Diesel-electric locomotive is thus a good road freight unit, capable of starting and handling relatively high trailing tonnages in comparison to its weight.

Diesel Electrification Versus Straight Electrification

Diesel electrification has become an important characteristic of all main-line railroads in the Pacific northwest. Whether to extend dieselization until it results in the complete elimination of steam facilities is the question of the hour. There is no longer much reasoning opposition to dieselization based on purely operational factors. Most competent observers agree that complete replacement of steam power would enhance the physical plants of the railroads of this region and result in considerable improvement in over-all speeds and tonnages. The question has broadened to one of engineering economics and therefore of alternatives. Assuming that some improvement in motive power was long overdue, it is at least debatable whether the change had to take the form it has. There is some possibility that the changing economics of fuel supply may force a revision of previous estimates of the attractiveness of complete dieselization of main lines and lead engineers to a more careful and open-minded consideration of the merits of coupling Diesel-electrification of yard and branch lines services with straight electrification of main lines.

Electrification advocates recognize that the Diesel-electric locomotive is really an electric locomotive which must carry its power plant with it. The word must is used advisedly, and Diesel-electric proponents could with some logic define a straight-electric locomotive as one that must leave its power plant on the ground. Whether a locomotive carries its generating source with it, or relies upon a central station for its source of energy is a question that involves at once the greatest strengths and the greatest weaknesses of both classes of motive power. Generation of power on the locomotive is an undeniable advantage so far as freeing the locomotive from dependence on a trolley system goes, but it must be recognized that at the same time it restricts the amount of power that can be efficiently produced, and, if the prime mover is a Diesel engine,

Table II. Distribution of Work Units Performed by Diesel-Electric Locomotives in Comparison with Total Work Units Performed by All Classes of Motive Power on Class I Railways in the United States

First Four Months of 1948[2]

Type of Service and Work Unit Employed	Total Work Units for All Classes of Motive Power	Total Work Units Credited to Diesel-Electric Locomotives	Per Cent Diesel-Electric
Yard Switching			
Freight, locomotive hours	18,695,055	6,263,544	34
Passenger, locomotive hours	1,480,780	710,073	48
Total, locomotive hours	20,175,835	6,973,617	35
Road haul			
Freight, thousands of gross ton miles trailing	451,880,341	84,151,283	19
Passenger, passenger train car miles	1,169,298,475	428,598,866	37

Table III. Diesel-Electric Locomotives on Class I Railroads in the United States—Comparison of Work Units Performed in Proportion to Numbers in Service

First Four Months of 1948

Type of Service	Proportion of Diesel-Electric Locomotives to all Locomotives, (From Table I)	Proportion of Total Work Units Credited to Diesel-Electric Locomotives, (From Table II)	Ratio of Work Performed to Proportionate Numbers of Locomotives in Service
Yard switching	24	35	1.46
Road freight	6	19	3.16
Road passenger	13	37	2.85

it limits the primary energy source to petroleum products of the middle distillate variety, the future availability and price of which is surrounded with so much uncertainty.

Although dependence on a trolley limits the scope of activity of a straight-electric locomotive somewhat, it nonetheless frees it from the limitations of relatively small locomotive-borne prime movers and widens the choice of primary energy sources to include coal and residual oil burned in central stations as well as hydroelectric power. In the Pacific northwest, with its substantial present and enormous potential development of hydroelectric power, together with its almost complete lack of petroleum, it is a little difficult to follow the reasoning employed by railroads in becoming more and more involved with a type of main-line motive power from which they can extricate themselves only with difficulty in the event of continuing adverse trends in the supply and price of Diesel fuel.

Comparison of Primary Energy Sources

It is the purpose of this paper to examine briefly the competitive position of coal, residual oil, Diesel oil, and electricity as primary energy sources for main-line railway operations in the Pacific northwest. For convenience of calculation, freight traffic only will be considered. It is of no great value to compare the cost of coal fired on one railroad with residual oil burned on another, or for that matter the cost of the same fuel burned on different divisions of one railroad. Even when the figures employed are expressed on the basis of thousands of gross ton miles, there is seldom a proper recognition of profile differences, relative ages, and efficiencies of locomotives and other operating variables that are the source of gross inconsistencies in the comparisons. It is proposed, therefore, to develop a set of equations that will permit determination of the quantities of each kind of fuel required when the same amount of work is done by different locomotives. The desired equations, to be useful and equitable as between energy sources, must be grounded in the basic physical relationships between force, distance, time, energy, work, power, and so forth, as conditioned by empirical experience in their utilization under actual operating conditions on railroads in this part of the country.

The fundamental physical relationships stated simply are:

$$\text{work} = \text{energy} = \text{distance} \times \text{force} \qquad (1)$$

$$\text{energy} = \text{power} \times \text{time} \qquad (2)$$

The basic units of equations 1 and 2 are feet, pounds, foot pounds, and foot pounds per unit of time. Practically, these units are little used and more familiar practical units will be employed. It is necessary to set up certain equivalents between the electrical, mechanical and thermal units involved. In some instances the equivalents used are not exact but are adequate for the purpose and have been chosen because they simplify calculations.

Energy units

One horsepower hour = 1,980,000 foot pounds

One kilowatt-hour = 2,640,000 foot pounds

Power Units

One horsepower = 33,000 foot pounds per minute
= 1,980,000 foot pounds per hour

One kilowatt = 44,000 foot pounds per minute
= 2,640,000 foot pounds per hour

Thermal Units

One British thermal unit = 780 foot pounds

Let

$KGTM$ = thousands of gross ton miles (including locomotives)

tr = average tractive resistance in pounds per ton

W = total work done against the average tractive resistance (tr) per thousand gross ton miles

Taking

Work = distance × force and converting to practical units (1)

$$W = 1{,}000 \times 5{,}280\ tr$$

Expressing W in heat units

$$W = \frac{1{,}000 \times 5{,}280}{780}\ tr \text{ (in Btu)}$$

$$W = 6{,}800\ tr \text{ (in Btu)} \qquad (3)$$

In using equation 3 it is necessary to assume some approximate heat values of coal, residual oil, and Diesel oil as received, and also to assume reasonable over-all utilization efficiencies. Such utilization efficiencies should be predicated not upon theoretical maximums but rather upon practical, every-day working conditions including recognition of the actual load factors that locomotives will be likely to achieve in main-line service in

Table IV. Illustrating Method of Computing Average Tractive Resistance Over Selected Sections of Railroad

Typical Pacific Northwest Profile

(1) Compensated Gradient, Per Cent	(2) Miles Per Hundred Miles, Per Cent	(3) Tractive Resistance Grade (Pounds/-ton)	(4) Tractive Resistance Traction (Pounds/-ton)	(5) Tractive Resistance Total (Pounds/-ton)	(6) Column (2) × Column (5) (Plus Values)	(6) Column (2) × Column (5) (Minus Values)
0.2	10	4	6	10	100	
0.5	10	10	6	16	100	
1.2	20	24	6	30	600	
1.6	15	32	6	38	570	
−1.4	15	−28	6	−22		−330
−0.9	20	−18	6	−12		−240
−0.5	10	−10	6	− 4		− 40
Total	100				1,430	−610

Average tractive resistance opposing movement = 14.3 pounds per ton. Value of tractive resistance (tr) used in equations 3, 4, 5, 6, and 7.

Average tractive resistance assisting movement = −6.1 pounds per ton. Value of tractive resistance (tr) (actually tractive effort) used in equation 8.

Table V. Comparative Tonnage Ratings—Late Model Steam, Diesel-Electric and Straight-Electric Locomotives

	Type of Locomotive		
	Steam (4-6-6-4)	Diesel-Electric 5,000 Rail HP	Electric 5,000 Rail HP
Total weight of locomotive (tons) (with 2/3 variable supplies)	488	460	360
Continuous tractive effort (pounds)	90,000	112,500	112,500
Total gross tonnage on 1.6 per cent grade (tons)*	2,368	2,960	2,960
Trailing tonnage rating on 1.6 per cent grade (tons) (line 3-line 1)	1,880	2,500	2,600
Ratio gross tons including locomotives to gross tons trailing (line 3 ÷ line 4)	1.26	1.18	1.14

* Line 2 + 38 pounds per ton total tractive resistance on 1.6 per cent grade.

the Pacific northwest. The efficiencies used below agree closely with the experience of a principal northwest railroad as revealed by test data assembled over a considerable number of years. The figures have been rounded off slightly in the interests of easy calculation.

Let

C = pounds of coal per $KGTM$ as burned in reciprocating steam locomotives

R = gallons of residual oil per $KGTM$ as burned in reciprocating steam locomotives

D = gallon of diesel fuel per $KGTM$ as burned in Diesel-electric road locomotives

Assume that the heat contents as received of the respective fuels are

Coal = 12,500 Btu per pound
Residual oil = 150,000 Btu per gallon
Diesel Fuel = 140,000 Btu per gallon

Assume further that the realizable heat contents and over-all utilization thermal efficiencies of these fuels are:

Coal = 680 Btu per pound and 5.4 per cent efficiency
Residual oil = 10,800 Btu per gallon and 7.2 per cent efficiency
Diesel fuel = 34,000 Btu per gallon and 24.3 per cent efficiency

Then substituting in

$$W = 6{,}800\ tr \tag{3}$$

$$C = \frac{6{,}800}{680}\ tr = 10.0\ tr \tag{4}$$

$$R = \frac{6{,}800}{10{,}800}\ tr = 0.63\ tr \tag{5}$$

$$D = \frac{6{,}800}{34{,}000}\ tr = 0.20\ tr \tag{6}$$

Table VI. Fuel and Energy Costs in Cents per Thousand Gross Ton Miles Trailing for Operations over Average Profile Shown in Table IV

	Reciprocating Steam, Coal Burning	Locomotives, Oil Burning	Diesel-Electric Locomotives	Straight-Electric Locomotives
Coal (12,500 Btu per pound as received)				
180 pounds at $4.00 per ton	36.0			
180 pounds at 5.00 per ton	45.0			
180 pounds at 6.00 per ton	54.0			
180 pounds at 7.00 per ton	63.0			
180 pounds at 8.00 per ton	72.0			
Residual oil (6,300,000 Btu per barrel as received)				
11.34 gallons at $1.50 per barrel		40.5		
11.34 gallons at 2.00 per barrel		54.0		
11.34 gallons at 2.50 per barrel		67.5		
11.34 gallons at 3.00 per barrel		81.0		
11.34 gallons at 3.50 per barrel		94.5		
Diesel fuel (140,000 Btu per gallon as received)				
3.37 gallons at 8.0 cents per gallon			27.0	
3.37 gallons at 9.0 cents per gallon			30.3	
3.37 gallons at 10.0 cents per gallon			33.7	
3.37 gallons at 11.0 cents per gallon			37.1	
3.37 gallons at 12.0 cents per gallon			40.4	
Electric energy				
40 kwh at 2.0 mills per kilowatt-hour				8.0
40 kwh at 3.0 mills per kilowatt-hour				12.0
40 kwh at 4.0 mills per kilowatt-hour				16.0
40 kwh at 5.0 mills per kilowatt-hour				20.0
40 kwh at 6.0 mills per kilowatt-hour				24.0

The prevailing Pacific northwest price (mid-1948) of each product is the middle one in each case shown above.

Fuel costs are delivered on locomotives.

Electric energy unit costs are based on deliveries at 3 phase, 60 cycles to railway substations, but quantities shown include transmission losses. Total costs of electric energy are therefore delivered on locomotives.

Equations 4, 5, and 6 are basic equations which will enable computation of the approximate quantity of each class of fuel required per thousand ton miles of operations performed against the same average tractive resistance under the conditions assumed.

In deriving an expression for electric energy requirements additional rotation is required as follows:

$RKWH$ = kilowatt-hours required per thousand gross ton miles as measured at the rail

$SKWH_m$ = kilowatt-hours required per thousand gross ton miles as measured at the substation (motoring)

$SKWH_r$ = kilowatt-hours required per thousand gross ton miles as measured at the substation (regenerating)

e = average over-all electrical efficiency between the rail and 3-phase 60-cycle supply sides of substations where power is delivered to railroads from central station sources.

Further let $e = 2/3$ which is a good realizable figure so far as Pacific northwest main-line railroad electrification is concerned.

Starting with

$$\text{Work} = \text{distance} \times \text{force} \tag{1}$$

$$SKWH_m = \frac{5{,}280 \times 1{,}000 \times 3}{2{,}640{,}000 \times 2} \times tr = 3.0\ tr \tag{7}$$

and

$$SKWH_r = \frac{5{,}280 \times 1{,}000 \times 2}{2{,}640{,}000 \times 3} = 1.3\ tr \tag{8}$$

$SKWH$ is the *net* substation kilowatt-hours per thousand gross ton miles and is obtained by subtracting the regenerative energy of equation 8 from the motoring energy of equation 7, but it should be noted that the tractive resistance (tr) used in the two equations is not the same. The tractive resistance used in equation 7 should be that opposing movement when motoring; whereas that used in equation 8 should be the negative tractive resistance (in reality a tractive force) which results from the downgrade component being greater than the Davis component, when the locomotive is in regeneration.

Comparisons Applicable to the Pacific Northwest

Profiles of Pacific northwest main lines vary in ruggedness, but most roads have ruling grades that reach 2.2 per cent compensated and many miles of all lines are on gradients in excess of 1.0 per cent. Individual analysis is required to determine the average tractive resistance and

Table VII. Comparative First and Operating Costs of Steam, Diesel, and Electric Locomotives, Operating Against Average Pacific Northwest Profiles

Costs Taken on Basis of Individual Locomotives Running 180,000 Miles Per Year and on Price Level Prevailing in Mid-1948

	Type of locomotive: Reciprocating steam				Diesel-electric		Straight-electric	
Primary energy source	Coal		Residual oil		Diesel oil		Hydroelectric energy	
Annual locomotive miles	180,000		180,000		180,000		180,000	
Gross tons trailing per locomotive mile	1,880		1,880		2,500		2,600	
First cost of items not common to all types of motive power								
Steam locomotives at $300,000	$300,000		$300,000					
Diesel-electrics at $125 per RHP					$625,000			
Electrics at $100 per RHP							$500,000	
Assumed pro rata first cost of electrification fixed facilities at 1.25 locomotive costs							$625,000	
Total first costs where differentials occur	$300,000		$300,000		$625,000		$1,125,000	
Operating costs	Per Mile	Per Year	Per Mile	Per Year	Per Mile	Per Year	Per Mile	Per Year
Locomotive repairs	$0.40	$72,000	$0.40	$72,000	$0.60	$108,000	$0.30	$54,000
Locomotive depreciation								
Steam at 3.0 per cent	$0.05	$9,000	$0.05	$9,000				
Diesel at 6.05 per cent					$0.21	$37,800		
Electric at 5.04 per cent							$0.14	$25,200
Electrification overhead costs								
Maintenance							$0.08	$14,400
Depreciation at 3.5 per cent							$0.12	$21,600
Fuel and energy								
339 lbs. of coal at $6.00 per ton	$1.02	$183,600						
21 gals. residual oil at $2.50 per bbl.			$1.25	$225,000				
8.5 gals. Diesel oil at 10 cents per gal.					$0.85	$153,000		
104 kwh electric energy at 4.0 mills per kwh							$0.42	$75,600
Lubrication	$0.01	$1,800	$0.01	$1,800	$0.08	$14,400	$0.01	$1,800
Total annual costs where differentials occur								
Per locomotive mile	$1.48		$1.71		$1.74		$1.07	
Per locomotive year		$266,400		$307,800		$313,200		$192,600
Per thousand gross ton miles trailing	0.787		0.910		0.696		0.412	

fuel and energy requirements over any particular section for which motive power comparisons are desired. To illustrate a suggested method of computing average tractive resistance, Table IV has been prepared. The profile used is believed to be average for the main line railroads of the region. It should be noted that the example in Table IV shows movements in one direction only which is assumed to be representative. Where precise calculations are being made for a specific section, the analysis should be extended to cover movements in both directions. Attention is called to another simplification employed in this analysis. The component of total resistance that is for traction only (Column 4) has been taken as 6.0 pounds per ton straight, in place of employing values varying with speed calculated from the usual Davis formulas. It has been found in actual practice in the northwest that the use of a straight 6.0 pounds per ton leads to conservative results as well as simpler computations. This practice should not be extended to other regions, however, where higher speeds and comparatively easier profiles are encountered. In this region, as the table indicates, the grade component is the one that exerts the major influence on total resistance rather than the Davis component. Finally, the average values of tractive resistance have been computed by expressing the profile in terms of miles per hundred miles, in other words, putting it on a percentage basis.

It is now possible to utilize the average tractive resistance values from Table IV and by substitution into equations 4, 5, 6, 7 and 8, obtain the physical quantities of the various primary energy sources required for the profile assumed.

The proper resistance values are:

14.3 pounds per ton
−6.1 pounds per ton

Taking equations in order

$$C = 10.0\ tr \tag{4}$$

$$C = 10.0 \times 14.3 = 143 \text{ pounds of coal per } KGTM$$

$$R = 0.63\ tr \tag{5}$$

Table VIII. Economic Evaluation of Steam, Diesel, and Electric Motive Power Operating Over Typical 400-Mile Division of Pacific Northwest Main Line

Traffic Consolidated in Terms of Freight Units Only

	Type of Motive Power: Reciprocating Steam		Diesel-Electric,	Straight-Electric,
Primary Energy Source	Coal	Residual oil	Diesel oil	Hydroelectric energy
Total annual traffic (KGTMT)*	5,760,000	5,760,000	5,760,000	5,760,000
First costs based on Table VII				
17.0 steam locomotives	$5,100,000	$5,100,000		
12.8 Diesel locomotives			$8,000,000	
12.3 electric locomotives				$13,837,500
Incremental first costs				
Over steam			$2,900,000	$ 8,737,500
Over Diesel				5,837,500
Annual operating costs based on Table VII				
Cost per KGTMT*	$0.787	$0.910	$0.696	$0.412
Total annual costs (where differentials occur)	$4,533,000	$5,242,000	$4,009,000	$ 2,373,000
Incremental annual savings				
Diesel over steam (coal)			$ 524,000	
Diesel over steam (residual oil)			1,233,000	
Electric over steam (coal)				$ 2,160,000
Electric over steam (residual oil)				2,869,000
Electric over Diesel				1,636,000
Rate of return on incremental investment for:				
Diesel replacing steam (coal)			18.1	
Diesel replacing steam (residual oil)			42.5	
Electric replacing steam (coal)				24.7
Electric replacing steam (residual oil)				32.8
Electric replacing Diesel				28.0

* Per thousand gross ton miles trailing.

$R = 0.63 \times 14.3 = 9.0$ gallons of residual oil per $KGTM$

$D = 0.20\ tr$ (6)

$D = 0.20 \times 14.3 = 2.86$ gallons of Diesel fuel per $KGTM$

$SKWH_m = 3.0\ tr$ (7)

$= 3.0 \times 14.3 = 42.9$ kilowatt-hour per $KGTM$

$SKWH_t = 1.3\ tr$ (8)

$= 1.3 \times (-6.1) = -7.9$ kilowatt-hour per $KGTM$

$SKWH = 42.9 - 7.9 = 35.0$ kilowatt-hour per $KGTM$

It is necessary to recognize that all of the foregoing figures are for thousands of gross ton miles including locomotives. But in moving traffic with different kinds of motive power, the varying weights of locomotives must be compensated for before a valid comparison of the relative quantities of fuel required to handle the trailing loads may be made. On a ruling 1.6 per cent grade, such as comprehended in Table IV, it is possible to arrive at a ratio between gross tons trailing and gross tons including locomotives by the method shown in Table V. This method involves using the characteristics of certain late model locomotives of the three principal types, the examples chosen being representative of motive power now in operation in the Pacific northwest. Applying the ratios of gross tons with and without locomotives developed in Table V to the fuel quantities already calculated yields the following:

$1.26 \times 143 = 180$ pounds of coal per $KGTMT$*

$1.26 \times 9.00 = 11.34$ gallons of residual oil per $KGTMT$

$1.18 \times 2.86 = 3.37$ gallons of Diesel fuel per $KGTMT$

$1.14 \times 35.0 = 40.0$ kilowatt-hours per $KGTMT$

* Indicates thousands of gross ton miles trailing.

In Table VI the quantities of the various primary energy sources expressed per thousand gross ton miles trailing have been extended against a range of unit prices for each source of energy. Five different unit prices are shown for each fuel with the prevailing or mid-1948 Pacific northwest price of each product being given the middle position.

Evaluation of Differential Capital and Operating Costs

In order to have a basis for comparing not only the fuel costs but the other annual costs including fixed charges which vary with the type of motive power, Tables VII and VIII were prepared. In Table VII the fixed and variable costs of all items that are contingent upon the kind of locomotives are shown. Single locomotives utilizing the four principal energy sources are given annual utilizations of 180,000 miles each. The only items of either first or operating cost that are considered are those that are dependent upon type of motive power. Items in which differential costs do not appear are disregarded and in this category are included a great many items which actually do vary with type of motive power but the variances of which are so slight and their calculation so involved that it would not add materially to the accuracy of the final evaluation to include them. The various first and operating costs assumed are believed to be conservative estimates based on actual prevailing price levels applicable to the Pacific northwest in mid-1948.

When all differential costs except return on invested capital are taken into account it is seen that there is a considerable margin in favor of main-line electrification.

Table VIII presents in very brief form a typical analysis of the possible returns on differential capital invested in dieselization and electrification. The method employed is to take a 400-mile operating division having a profile that calls for an average energy expenditure equal to that developed in Table IV and apply the cost figures developed in Table VII. An annual traffic volume expressed in terms of freight units exclusively is assumed equal to 5,760,000 thousands of gross ton miles trailing. This is equivalent to a daily movement of about 20,000 gross tons trailing per day in each direction over the 400-mile division. Utilization of all four locomotives concerned has been assumed at 180,000 miles per year, and no provision made for spare units. The number of each type of locomotive required was determined from the tonnage ratings of Table V.

The return on differential capital investment expended for main-line electrification appears to be very generous indeed and points up the remark made earlier in the paper that railroad managements should be giving serious consideration to using indigenous sources of energy, which in the Pacific northwest means that careful consideration be given main-line electrification employing hydroelectric power.

References

1. Statement M-240 (OS-F). Bureau of Transport Economics and Statistics, Interstate Commerce Commission, (Washington, D. C.) April 1948.

2. Statements M-211 (OS-A); M-213 (OS-B); M-215 (OS-C). Bureau of Transport Economics and Statistics, Interstate Commerce Commission (Washington, D. C.) April 1948.

No Discussion

Handling Suburban Traffic on Steam Railroads

M. L. SLOMAN
NONMEMBER AIEE

SINCE 1940 the volume of suburban commuter traffic on Class I railroads has increased to such an extent that the few railroads heavily engaged in this business have found it difficult to provide the quantity and quality of service demanded by the public. Increased activity in the electric multiple-unit car equipment field is evidence that, given adequate rates, the railroads intend to meet this demand and to obtain their share of new patrons as the commuting public grows larger. Different types of motive power are discussed with respect to their ability to meet or improve the fundamental operating conditions common to all suburban roads. Electric multiple-unit cars have superior operating characteristics and are best suited to provide improved service at minimum expense. The various types of equipment and methods of operation are discussed in regard to the three general classes of existing power systems and voltages used in this service.

Volume and Nature of Traffic

Many years ago the steam railroads of the United States had a virtual monopoly of the suburban traffic. However, the mass production of automobiles and busses combined with better roads, superhighways, and socialized rapid transit systems steadily reduced the volume of this railroad business from 7,000,000,000 commutation passenger miles in 1926 to a low of 4,000,000,000 in 1940. By 1946, the last year for which figures are available, this traffic had increased to almost 6,000,000,000 passenger miles (Figure 1).

While part of this gain was a natural effect of the high rate of employment during the war, a very substantial gain of almost 500,000,000 commutation passenger miles was recorded for the year 1946 over 1945, following the end of both the war and gasoline rationing. This figure is even more significant because many of the big industries were on strike for as long as three months during the year, and new automobiles were on the market for the first time since 1941.

Many causes contributed to this marked increase in suburban traffic, but at least three are outstanding. First, the greatest part of the postwar new home projects are being built in the suburban sections of the large metropolitan areas, and in some cases these communities are 15 to 20 miles from the downtown business center. Second, a fairly high proportion of the downtown business population returned either from the armed forces or from factory jobs in war plants located generally in the outlying sections of the larger cities. Third, the totally inadequate parking facilities in the business districts of all cities have practically forced all commuters and shoppers to seek some means of public transportation. A recent survey indicated that the cost of providing adequate parking facilities is prohibitive, neglecting the tremendous cost for additional street space to handle the huge potential volume of automobile commuter traffic.

Few railroads have enough volume of commuter traffic to warrant large expenditures for improvements. But those that are strategically located for serving the densely populated urban sections of large cities realize that there is a growing demand for this type of service as evidenced by the recent interest in purchases of new equipment. Hence, there is a real need for a review of the operating characteristics of the several types of motive power used so that new equipment may be applied to give the best possible service at the least expense. Of the many factors involved in making the best motive power application for a suburban service, most important and fundamental are the following operating conditions: volume of traffic; maximum schedules; average length of run; maximum speed; maximum train to be handled; and maximum flexibility of motive power.

All suburban operations place a severe burden on the railroad due to the poor load factor which results from the heavy rush-hour peaks occurring for relatively short periods twice each day. These peaks are of such magnitude that the rolling stock required to provide for them is three to six times that for the base load, resulting in very low annual mileage for suburban cars compared with that of cars in through service. As a result of this low annual mileage, the largest item of expense becomes the fixed charges. As a general rule, it is sound economy to expend liberally for equipment which is used continuously, but when it can be used only for short periods, less expenditure is justified. From this standpoint, an electrified multiple-unit operation is

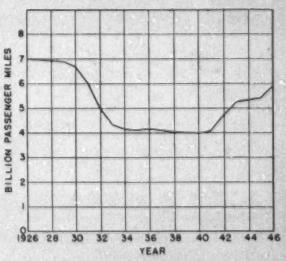

Figure 1. Annual commutation passenger miles for Class I roads (Interstate Commerce Commission reports)

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M. L. SLOMAN is with the industry engineering department of the Westinghouse Electric Corporation, East Pittsburgh, Pa.

ideal, for if the minimum number of motor cars is provided to handle the rush-hour load, a sufficient number will be available for the best possible service during the nonrush hours. The most economical method of operation is the largest possible use of trailers during the rush-hours to reduce the investment for equipment which has a poor load factor, and the use of all motor cars during the nonrush hours to provide the fastest service to attract new patrons. Any increase in traffic during the base period should result in practically net profit since no additional operating expenses are required. As the equipment is available, no additional investment is required; and power is less expensive at these hours, since the load factor is improved and the demand is not increased.

When steam, Diesel, or electric locomotives are used, a large initial investment in motive power is required to handle the rush-hour load, and the fixed charges per mile become burdensome due to the poor utilization of the excess motive power during nonrush hours. However, an eastern road with a large volume of suburban traffic recently placed in service three double-end 2,000 horsepower Diesel-electric locomotives which are regularly assigned to service approximately 320 hours per month in scheduled running times. These locomotives have performed very well and have averaged 10,000 miles per month, but it is doubtful if more will be purchased because additional units could not be utilized to the same extent during the base periods of traffic. On a completely electrified division, there is a field for locomotive-hauled trains on suburban express runs provided the locomotives are available between assignments on through trains.

It is necessary to analyze the schedules and service conditions of the present or proposed new service before the proper accelerating rate and maximum safe speed can be determined. In a recent study of a large suburban operation, the data shown in Table I were recorded although conditions unquestionably will vary on different railroads.

Table I

	Per Cent of all Runs
Runs less than 1 mile*	40.0
Runs from 1.0 to 1.4 miles	23.5
Runs from 1.5 to 1.9 miles	14.3
Runs from 2.0 to 2.9 miles	11.4
Runs from 3.0 to 4.9 miles	7.8
Runs from 5.0 to 9.9 miles	2.2
Runs more than 10 miles	0.8

* A run is taken as the distance from one stop to the next stop, as shown by the timetable.

The average distance for all runs was found to be 1.70 miles, therefore, equipment should be applied with particular emphasis to a 1.70 mile average distance between stops. At the same time ample capacity for the shortest runs and ample speeds for the longest runs also should be provided.

Accelerating Rates

The upper limit of acceleration is approximately 1.5 miles per hour per second (mphps) when 2-motor equipments are used with all cars in the train motorized. For example, to accelerate a 60-ton car with a seated load of seven tons at a rate of 1.5 mphps requires an average tractive effort of 10,200 pounds. The same tractive effort will be delivered with an empty car since automatic current limit control is generally used in multiple-unit service. The weight on drivers with a 2-motor equipment is about 55 per cent, or 66,000 pounds, giving an average nominal adhesion of 15.5 per cent. However, during acceleration the peak tractive effort delivered will be about 15 per cent greater than the average, increasing the nominal adhesion to 17.8 per cent. In addition, there will be a certain amount of weight transfer, bringing the adhesion on the light axle above 20 per cent. Higher rates of acceleration are obtainable for equipment of this type and service, but these can be maintained only under good track conditions such as found in subway service where 2-motor equipment is operated at a 1.75 mphps rate.

Steam and Diesel-electric locomotives are not capable of accelerating trains at a 1.5 mphps rate due to the limitations of their relative adhesive characteristics. Figure 2 shows typical accelerations for 10-car trains. The multiple-unit train will accelerate at a 1.5 mphps rate to 25.5 miles per hour (mph) compared to 0.673 mphps to 8 mph for the Diesel-electric locomotive hauled train and 0.458 mphps to 10 mph for the steam locomotive-hauled train. While the rates will vary for the locomotive-hauled trains depending upon the number of cars, a constant accelerating rate can be maintained with multiple-unit operation regardless of the number of cars in the train, providing of course that either all cars are motorized or that a constant ratio of motor cars to trailers is maintained. On a recent installation of Diesel-electric motive power replacing steam, a considerable reduction in schedules was made due almost entirely to the better accelerating characteristics of the Diesel-electric. On very short runs in local service, a greater improvement in schedule speed is obtained by raising the accelerating rate than by any other method. For this reason, multiple-unit cars will provide faster service, not only because of their high accelerating rates, but also because they will maintain these rates during the rush-hour periods when 10- to 15-car trains are operated. This is not possible with locomotive-hauled trains where the accelerating rate varies inversely with the length or weight of train.

Maximum Safe Speeds

The maximum safe speed for which the motors must be geared is of prime importance in electric or Diesel-electric operation. In the application of new equipment, future operation as well as present schedules must be considered, for operating conditions may change materially during the life of the equipment which experience shows may be as high as 30 years. Gearing for a maximum safe speed of 70 mph with worn wheels represents the desirable minimum, for an extensive suburban operation will comprise a diversified service in which the average length of run per train ranges from one mile or less, to three miles or more. High speeds are not essential for local service where stops are made at every station from the terminal to the end of the run, but they are necessary for the long runs in express service and raise the schedule speed in skip-stop service which is becoming more popular.

Higher Schedule Speed

As most of the new commuter traffic and part of the old is now derived from the new areas which are being built farther from the downtown business section, many railroads now operate trains with stops at every station at the beginning of an inbound run for perhaps seven or eight stops, and then nonstop for distances ranging from five to ten miles to the terminal. Another group of seven or eight adjacent stations will be served by another train in the same manner with a consequent increase in schedule speeds for all trains on the line. Proper scheduling must be done so that all trains can run at high speeds on the express sections of the run, but this is difficult unless trains can run on two tracks in the same direction.

Skip-stop service is a more practical method of train operation where only one track is available for one direction. Faster and more frequent service can be provided and the average distance between stops can be raised three-fold. In order to

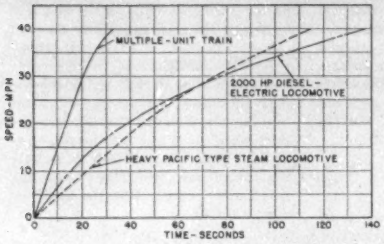

Figure 2. Acceleration curves, 10-car trains, 67 tons per car, level tangent track

take full advantage of the increase in length of average run, a high maximum safe speed is desirable to obtain a higher schedule speed.

Calculations have been made for multiple-unit train operation on a 1.3 mile run with various accelerating rates and maximum safe speeds. For a given increase in horsepower the improvement from higher accelerating rates is approximately the same as that for higher maximum safe speeds. As the power consumption is less when obtained through higher accelerating rates, this is the more economical method. The main point brought out by these calculations is the relatively small improvement possible in schedule speeds on short runs, even with large increases in horsepower capacity. By increasing the accelerating rate from 1.0 to 1.5 mphps and the maximum safe speed from 70 to 90 mph, the schedule speed is raised only 6.6 per cent. To effect this improvement requires 67 per cent more horsepower and 14.3 per cent more energy consumption. By raising the accelerating rate from 1.0 to 1.5 mphps and maintaining the 70 mph safe speed, a better schedule of only 3.3 per cent results with a 4.7 per cent increase in power consumption. Due to the larger capacity equipment, an increase in maintenance expense can also be expected.

It is doubtful if the results justify the additional expense, considering the very small improvement obtainable in short runs. When trains operate continuously with a uniform consist on uniform headways, decreased running time generally enables a reduction to be made in the total number of cars. However, in suburban service, where the rush-hour peaks occur twice daily for periods of approximately two hours and about 70 per cent of the cars are required only during these periods, the possible reduction in running time is far too small to permit more than one round trip to be obtained per car. Consequently, no reduction is possible in the total number.

Assuming that the average passenger ride is 30 minutes, the 3.3 per cent improvement in schedule speed referred to previously would mean a saving of only 59 seconds in the riding time. The operation of skip-stop service or express runs to or from a group of adjacent stations is far more advantageous than high accelerating rates and larger motor capacity.

Effect of Train Length

Another factor which must be considered in the application of new motive power is the maximum length of train to be handled. Due to the nature of the traffic, a railroad can determine quite accurately the number, destination, and arrival or departure times of its commuter patrons. With these data together with known track capacities, signal spacings, and platform lengths, it can determine the total number of cars and trains required to handle the traffic. Most large suburban operations now run 10- to 15-car trains during the rush-hours and 4-to-8-car trains during the base periods. Platform lengths frequently limit the number of cars per train, and for this reason, plus the fact that the operating cost per seat mile decreases as the seating capacity of the car increases, some roads use heavy cars with a 3-2 seating arrangement of 120-passenger capacity. Another road has designed and built double-deck cars with seats for 134 passengers. These cars are equipped with motors for multiple-unit operation, and have operating characteristics which permit their training with older cars in varying consists, and at the same time maintaining a constant accelerating rate and balancing speed to give identical service in both rush-hour and base periods of traffic. This is one of the decided advantages of electric multiple-unit car operation over steam, Diesel-electric, or electric locomotive-hauled trains. Even if it were permissible to operate any form of motive power in the New York region, it is very doubtful if steam or Diesel-electric power could economically handle the traffic without large-scale expenditures for additional main tracks to handle the increased number of shorter trains required to meet the present schedules.

Operating Flexibility

Maximum flexibility is another consideration which, to a large extent, governs the expenditure required for motive power and rolling stock. Undoubtedly, a minimum of terminal switching is desirable, but to obtain it is expensive. Serious study, therefore, must be given to justify any additional expense incurred in obtaining flexibility. With electric multiple-unit cars, three methods of operation are possible: all motor cars; semipermanently-connected 2-car units consisting of one motor car and one trailer; and motor car and trailer operation in varying consists limited to a minimum fixed ratio of motor cars and trailers. Obviously, all motor car operation gives maximum flexibility and the greatest protection of equipment and schedules in case of failure on one car in a train. However, these advantages are offset somewhat because of the large initial investment in motive power, and the high fixed charges due to poor utilization. Semipermanently connected 2-car units give almost the same high degree of flexibility, but with less protection of equipment and schedules with lower costs. Motor car and trailer operation gives the least amount of flexibility. It increases terminal switching costs and provides a minimum of protection, but substantially lowers operating costs. All of these operating methods are in use successfully on large suburban properties throughout the world. All have their advantages dictated largely by the conditions peculiar to each individual railroad.

Most postwar purchasing of new equipment has been for all motor car operation. Some of this equipment was for replacement purposes on roads where this type of operation had been in use for many years. The remainder was purchased to carry the increased commuter traffic, and any of the three methods of operation could have been used by keeping the new equipment in a separate pool.

Today, the railroads are striving more and more to reduce the cost of operation in spite of inflated labor and equipment costs. Increased revenue is essential, but usually considerable time elapses before a requested increase in rates is granted. Hence, the belief is prevalent that the maximum flexibility obtained with a consequent reduction in terminal facilities and costs justifies the additional investment required for all motor car operation.

Moreover, fixed charges and operating expenses can be lowered by maximum utilization of the new equipment in both base and peak service, and augmenting the latter with existing cars.

Another reason for all motor car operation is the public demand for faster and more deluxe commuter service in keeping with the latest trends in through service. As a result, new lightweight, semistreamlined commuter cars equipped with air conditioning, automatic heating and ventilating controls, fluorescent lighting, better seating, and washroom facilities have increased tremendously in cost over the pre-war equipment. The additional cost of traction motors and controls is now a much smaller percentage of the total cost of the complete car.

Effect of Existing Power Systems

It has been shown that electric multiple-unit operation of suburban service is the most advantageous from every operating standpoint including economy, provided the volume of traffic warrants the high initial cost of a complete electrification. The application of new equipment on an existing electrified railroad is influenced to a considerable extent by the voltage and power system, whether alternating or direct current. This is particularly true in regard to the number of motors per car and to the method of train operation, whether all motor cars or trailer operation. Since no new suburban electrification projects are planned in this country, all activity in this field will be limited to existing systems for many years. Therefore, it is well to consider how the type and voltage of the power system influences the selection of multiple-unit equipment.

Low Voltage D-C Systems

All suburban electrifications can be grouped into three general systems: namely, low voltage direct current at 600-650 volts; high voltage direct current at 1,500 or 3,000 volts; and high voltage alternating current at 11,000 volts. Two Class I eastern roads use the low voltage d-c system and operated 52,000,000 passenger car miles in 1946 with multiple-unit trains.

From the standpoint of motor car equipments only, the 600 volt d-c system offers the minimum in weight and cost. The motors can be constructed with a very low weight, the control is quite simple, and the switches are small since no high voltages are encountered. A modern 2-motor equipment of this type weighs 13,300 pounds—9,300 pounds for the motors and 4,000 pounds for the control equipment.

Two 600 volt d-c axle-hung motors per car, mounted on one truck, are in general use. Series parallel operation is used with fixed rate, automatic control equipment providing smooth accelerations with rates up to 1.5 mphps. This accelerating rate should not be exceeded as adhesion values above 20 per cent do not give adequate protection against wheel slipping in service when frequent starts are required under bad track conditions.

It is also possible to use on each car four 300-volt series-wound self-ventilated spring-borne motors with single reduction drive, field control, eand insulated for operation two in series and two groups in parallel on 600 volts. With 4-motor equipment and all axles motorized, full advantage can be taken of the total car weight for higher accelerating rates with only a nominal factor of adhesion. Recently, this type of equipment has been applied in subway service at a 2.5 mphps accelerating rate. On short runs and excellent track conditions, this feature is desirable to maintain high schedule speeds. Dynamic braking with a 4-motor equipment can provide a higher practical brak ing rate with a consequent increase in schedule speed. Subway and rapid tranist service with average runs of a half mile in local service differs greatly from suburban service where the trend is toward increased distance between stops. The increased first cost and maintenance expense of the 4-motor equipment, therefore, is difficult to justify in long-run high-speed service. A 4-motor 600-volt d-c equipment weighs 14,200 pounds—10,800 pounds for motors including gear units and 3,400 pounds for the control apparatus which includes a motor-generator set.

High Voltage D-C Systems

Two Class I roads use the high voltage d-c system, one at 1,500 volts and the other at 3,000 volts. Together these roads operated almost 21,000,000 passenger car miles in electric multiple-unit service in 1946. The use of 1,500 volts makes desirable, although not necessary, cars with four motors, thus permitting two 750-volt motors to be permanently connected in series with two groups in parallel. This arrangement gives two running notches with no resistance in the circuit, thereby reducing power consumption by the elimination of the resistance loss which would occur in the half-speed operation of a 2-motor equipment. To make the 4-motor equipment economical, trailer operation is essential using a semi-permanently coupled motor car and trailer unit. This type of operation not only gives good performance at a lower power cost than with all motor car trains of 2-motor equipments, but also reduces the first cost by eliminating the need for control equipment on the trailer car. A 4-motor equipment weighs approximately 30,000 pounds—20,000 pounds for the motors and 10,000 pounds for the control equipment. However, no electric equipment for traction purposes is required on the semipermanently coupled trailer, thus, the total electric equipment weight for the 2-car unit compares favorably with that required for two 600-volt d-c cars each equipped with two motors and control apparatus.

A 3,000-volt system requires four motors per car with the two 1,500-volt motors connected permanently in series. This likewise results in a 2-car (motor car-trailer) operating unit for economical reasons. This equipment is heavier and more costly than those for the 1,500- or 600-volt d-c systems, the electric equipment alone weighing approximately 41,000 pounds. The control equipment must be large and heavy to handle the high voltage. Inductive shunts, weighing about 3,000 pounds are required, and some sort of rotating apparatus is necessary to furnish the proper low-voltage control circuit power. Both railroads with 1,500- and 3,000-volt d-c systems operate multiple-unit trains at a 1.5 mphps accelerating rate and 70 mph maximum safe speed.

High Voltage A-C Systems

Three Class I roads use the a-c 11,000-volt system, recording 36,000,000 passenger car miles in multiple-unit service in 1946. Two roads use 2-motor equipments in all motor car operation, while the third uses 4-motor equipments with trailer operation, because part of its suburban service must be run on a 600-volt d-c third-rail system. This necessitated both a-c and d-c control equipment. It was believed at the time of installation that the additional expense involved in the large amount of control apparatus could be reduced by equipping only one of every three cars as a motor car. A-c equipments are heavier and more expensive than those for 600- or 1,500-volt d-c systems, but weigh less and are about equal in cost to the 3,000-volt d-c system. A recent 2-motor equipment weighed 26,600 pounds—11,500 pounds for the motors and 15,100 pounds for the control apparatus. A-c motor weights compare

favorably with 650-volt d-c motor weights, the former being about 15 per cent greater on a pounds-per-continuous-horsepower basis. However, a-c control equipment weights are much greater than those for the 600-volt d-c cars because of the heavy transformer required which, in turn, necessitates heavier car floor and center sill construction.

Noncommuter traffic in 1946 was down 35 per cent from 1944, but commuter business was up 11 per cent for the same period. The potential volume of this traffic should increase as residential areas develop at greater distances from the central business districts. By providing adequate automobile parking facilities at suburban stations, faster schedules, and more frequent service with high-speed modern electric multiple-unit equipment, the railroads in the high volume commuter business can expect a large share of this increased traffic without proportionate increases in operating expenses.

No Discussion

Large Indoor Power Air-Blast Circuit Breakers

G. E. JANSSON
NONMEMBER AIEE

Synopsis: This paper deals with some of the electrical and mechanical engineering problems encountered in developing large power air-blast circuit breakers. The characteristics and adaptability of the axial blast and cross-blast types of circuit breakers, and the reason for utilizing part of the latter principle for the design of large, indoor, medium voltage power circuit breakers will be discussed. A brief review of circuit interrupting phenomena, resistor application, air flow conditions, as well as general dimensions and outline, will be given. Finally, analysis and test data will be submitted indicating the possibility of further development of the high power cross-axial blast circuit breaker. That is a circuit breaker in which the contacts part at right angles to the blast, while the arc is immediately attenuated in the axial direction of the blast so that interruption occurs as in an axial-blast circuit breaker.

Axial Blast Versus Cross Blast

IN the early experiments with the axial blast circuit breaker, we came to the conclusion that the European design of these types of circuit breakers could, by proper modification of the interrupting element, be made to satisfactorily interrupt very large blocks of power.

We did not share the feeling of some contemporary investigators that an upper ceiling existed for the energy that could be interrupted by the nozzle type of interrupting element. As early as 1942, P. L. Taylor and H. W. Martin, after exhaustive tests, stated in an AIEE paper,[1] as follows:

"The evidence indicates that no such fixed ceiling or point of breakdown of relative values exists, and that still further development and extension of the potentialities of the device may be expected."

This statement has received further confirmation both by our own tests, and the successful operation of circuit breakers in service, as well as by tests carried out by European designers.

We therefore wanted to retain the axial blast principle for interruption, but for reasons enumerated later, desired to adopt certain structural features of the cross blast design, such as greater ease of design for large ampere carrying capacity by the use of conventional multiple bar and finger type contacts, making the cross-blast circuit breaker more adaptable to the high power requirements of this class of circuit breaker.

The combination of interrupting and isolating contacts in one moving member gives greater ease and freedom in the design of the sequence co-ordinating mechanism for the necessarily heavy current carrying members required.

The cross-blast structure appears somewhat easier to adapt to conventional switchgear and cell mounting. Another factor is the easy inspection of contacts, particularly as the blade or moving contact can be visually inspected when the circuit breaker is in the open position. The inspection of this contact will give a general idea of the condition of the finger contacts as well.

A close study of arc transfer during interruption in an air-blast circuit breaker shows that the arc starts at right angles to the blast and then is elongated in the axial direction of the air stream when final interruption occurs (Figure 1).

It is well recognized that the main structural advantage of the axial-blast circuit breaker over the cross blast is its easier adaptability to high-voltage insulation, particularly for outdoor applications. This is because the interrupting chambers can be fully enclosed in porcelain tubes. The axial-blast type of circuit breaker thus is ideal for high and supervoltage applications and for installation outdoors where excessive dust or corrosive fumes are encountered. In this class of circuit breakers the carrying current ability required does not exceed values which can be carried readily on butt type of contacts.

In the indoor, high power medium voltage class of circuit breakers carrying currents of 2,000–4,000 amperes are common, requiring special multifinger contacts in order to keep the temperature low enough to prevent damaging oxidation.

Review of Interrupting Devices

In looking through the technical literature for the last 10 to 15 years, it is surprising to find so many different versions of designs, all of which evidently have been brought to satisfactory performance. It, therefore, appears that the particular arrangement used is not of prime importance, but rather that the proper dimensioning and configuration of the device chosen, within reasonable limits, are the deciding factors. In a recent survey, the writer collected 25 different designs, all of which are in service, and most of them sufficiently different so that one would expect considerable variation in interrupting performance.

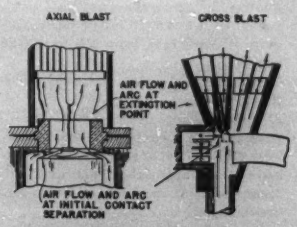

Figure 1. Comparison of axial and cross-blast circuit breaker arcs

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G. E. Jansson is with the Allis-Chalmers Manufacturing Company, Boston, Mass.

In attempting to evaluate the action of a stream of air surrounding an arc axially with the flow direction with that of an arc held perpendicular with the flow leads us to the following deductions.

Assuming that thermal energy ions are diffused radially from the arc. Then as shown in Figure 2, the force acting on these particles in the axial blast would be

$$F=\sqrt{f^2+d^2}$$

when

f = air flow force and d = the diffusion force.

At a distance x from the axis, this force would be uniformly acting on $2\pi x p$ particles. Now in an arc held perpendicular to the flow, the positive and negative components cancel out and the remaining radial vectors would vary from 0 to d as shown according to the formula $d \cos \alpha$ and the force at any point x is

$$F=\sqrt{f^2+(d \cos \alpha)^2}$$

This force again acting on $2\pi x p$ particles. However, since $d \cos \alpha$ varies from $d=d$ to 0, it is seen that the resultant force is considerably smaller than for the case of the arc parallel to the stream of air. Physically, the difference may be somewhat smaller than indicated by the above figures due to other factors influencing the result.

For this reason, some five different types of arcing structures were made up, and preliminary tests were run to obtain some basic data for the final interrupting chamber designs. Two of these designs developed insulation weaknesses which led to failures at relatively low currents. One device failed on the fifth CO (close-open) shot at about 19,000 amperes and 11,000 volts, single phase. This device had previously interrupted 12 CO shots at varying currents from 500 to 11,500 amperes at 13,000 volts across the single-phase break.

One interrupting device interrupted 20 single-phase CO shots at from 500 amperes to 38,200 amperes without failure. Another device of the cross-axial blast design interrupted 25 single-phase CO shots at currents of from 500 to 55,000 amperes, as shown in Table I, all without failure, or arcing times indicating serious stress.

Without change in contours or dimensions, the indications on these two devices were that the limit in interrupting current had not been approached as far as it had been possible to carry out the test in the time allotted.

A careful analysis and study of the test records, however, indicated that still further improvement in performance and increase in interrupting capacity may be obtained by slight modification of dimensions and contours of the device finally adopted for production. The preliminary tests confirmed the interesting facts that with the correspondingly proper dimensioning and configuration, practically the same interrupting performance could be obtained with devices of widely different design.

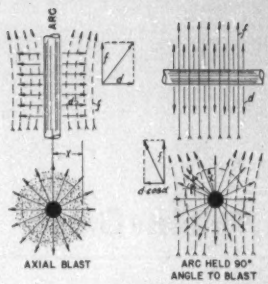


Figure 2. Thermal convection and air-blast forces on arc particles

Air Flow Characteristics

During these test series considerable difference in air flow characteristics was observed from that of the axial-blast circuit breakers. The reason for this difference apparently is the open to atmosphere construction of the cross-blast device, in contrast to the practically closed nozzle of the axial blast design. In the former, the flow and pressure follows somewhat that of a Laval nozzle. In the latter, an oscillatory pressure wave results.

Figure 3A illustrates the approximate air flow and pressure conditions of an axial or nozzle-type of circuit breaker, and Figure 3B shows that of the cross-blast design. These diagrams indicate readily that in the axial blast with the initially closed nozzle arrangement, the air pressure will momentarily rise to a certain value, and then oscillate down to a steady condition in which a relatively slow uniform decrease of pressure with time occurs. The maximum magnitude of the momentary pressure peak may considerably exceed the tank pressure, depending on length and volume of feed line between main blast valve and nozzle, as well as amount of air leakage around the nozzle contact itself.

In other words, when the air blast reaches the nozzle, a pressure wave is reflected from the nozzle towards the tank in the same way as an electric potential wave is reflected from the open end of a long transmission line. This reflected pressure wave is superimposed on the tank pressure.

So far we have discussed the air flow and pressure in an axial-blast circuit breaker at no load. Now, what happens to this pressure and flow when an arc is interrupted? Since the arc is at a very high temperature, that is in a state of excessive thermal energy, it follows that the arc must be surrounded by a high pressure zone. Now, if the current is increased to a sufficient value, depending on the diameter of the nozzle so that this high pressure zone, which may considerably exceed the tank pressure, completely fills the nozzle it is clear that the air flow will be stopped and a pressure rise as described previously would occur. As a matter of fact, at a sufficiently high current the flow of air is not only stopped at the nozzle but may actually reverse, flowing towards the tank. This usually leads to failure as the time lag for rectifying this reversed pressure wave usually exceeds the time interval of zero current.

In the cross-blast type of circuit breaker the air flow passage in the conventional designs are relatively open to atmosphere, and the no-load air flow conditions, therefore, more or less assume the characteristics of the flow in a Venturi tube or DeLaval nozzle.

This means that nodes or points of varying pressure and flow velocities are created. When, however, a high power

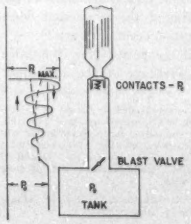


Figure 3A (left). Axial-blast circuit breaker air flow

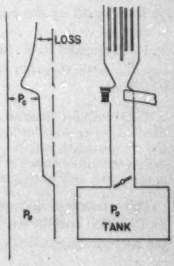


Figure 3B. Cross-blast circuit breaker air flow

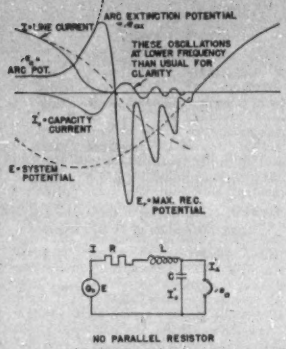


Figure 4. Current and voltage without parallel resistance to arc

arc is drawn a stoppage of air, or even a reversal, as described before for the axial-blast circuit breaker may take place. The energy value at which this occurs depends on the dimensions and configurations of the air passage. In the cross-blast circuit breaker this air flow stoppage and accompanying pressure rise can occur only after the contacts have been separated and the arc drawn for some time.

One of the troubles we encountered in some of our first experimental arc interrupting structures was a too open front exhaust along the moving blade which required correction before successful operation could be obtained.

A review of available literature on flow of compressible fluids indicated a rather mathematically complex solution, and then only of some rather specific and limited construction such as the Venturi or Laval nozzle, and so forth.

Some idea of the effect of harmful eddies and contractions can be obtained from pictures of flow of noncompressible fluids, such as water with aluminum powder for visibility. However, these are not directly applicable to compressible liquids, and are at most only a rough guide. Pressure and air velocity measurements are the only sure way to obtain reasonably accurate data on a particular structure.

An approximate ratio of effective nozzle area to main blast valve area of 1 to 3, or 4 appears required to obtain approximately full tank pressure on the momentary pressure peak.

Blast Valve

The component of the circuit breaker that controls the air flow is the main blast valve. The design of this important item is a major problem in itself. Several specific requirements have to be fulfilled to give maximum efficiency and serviceability. For instance, it must be fast acting, it must have a minimum pressure drop across it, and a minimum energy loss due to eddies. Its main parts must be readily accessible for inspection and exchange. It must be sufficiently rugged to withstand thousands of operations without servicing or renewals.

In our early designs of air-blast circuit breakers we followed the European design of pressure operated valves. This gave a very rugged valve with excellent air flow characteristics. In larger sizes, however, it became heavy and awkward. Because of this, inspection and change of valve seats became a consumer of time and manpower. This design was therefore superseded with a differential valve.

At the present time, however, further development of the direct pressure operated blast valve has been carried out to a point where the same easy access for inspection and servicing can be obtained. Due to its inherently faster operating time it may eventually supersede the differential type of valve.

The reason for the longer mechanical opening time required by the differential type of valve is the large dead air space under the valve which must be evacuated before action takes place. For instance, computation shows that the time in cycles to equalize the pressure under the piston of a 4-inch diameter differential valve with as small a dead air space as practicable would require 1.63 cycles, whereas the time to build up sufficient air pressure to begin movement of a pressure piston operated valve is of the order of 0.402 cycles. In both times figured a 1/2-inch diameter pilot valve was considered. The opening time of this pilot valve is not included.

As mentioned before, an advantage of the cross-blast circuit breaker is the inherent fixed mechanical sequence between circuit interruption and isolation. An equally important and easily obtained fixed mechanical or pneumatic relation may be had between contact separation and main blast valve opening. Approximately 3 cycles or less time from energizing trip to contact separation with an average arcing time not exceeding 1/2 to 1.0 cycles can be obtained readily.

Contacts

Contact arrangement for the cross-blast circuit breaker follows along the conventional blade and finger design. However since in the cross-blast circuit breaker the same contacts are used for both opening and closing operations, a rather different and severe problem of momentary current transfer without undue preheating and contact burning is introduced. Mechanical chattering, as well as magnetic blowoff of the contacts at high current values must be fully counteracted to prevent serious contact errosion and possible failure.

Through a careful analysis of a large number of contact making tests, we were able to reasonably well define the contact form, spring placements and pressure which eliminated the chattering and burning even on high current shots.

Parallel Resistance

The probable current and potential phenomena occurring during interruption of an arc in a circuit containing inductance and capacity is shown in Figure 4. In this it is assumed that due to the ever-increasing gap distance with its extension of arc length, cooling and scavenging action of the blast, and so forth, the current tends to decrease at a faster rate than given by $Zi = I\sin\omega t$.

The difference between the actual current and the current given by the foregoing figure is in the form of an oscillating charging current Zi_c which oscillates with a frequency of $f = 1/2\pi\sqrt{LC}$. The damping out of the oscillations is dependent on the circuit and arc resistance. The arc voltage, that is voltage across the circuit breaker gap, increases with the decrease of the instantaneous value of the current and finally reaches the extinction value e_{ax}. Instantly, the system voltage attempts to establish across the contact gap, but due to the oscillating charging current mentioned before an oscillating voltage wave is produced which, super-

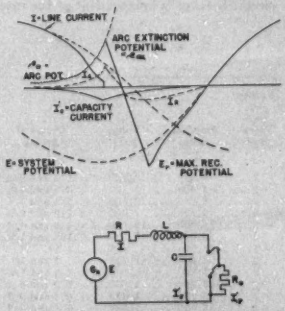


Figure 5. Current and voltage with parallel resistance to arc

imposed on the system voltage, gives rise to a voltage peak E, as shown.

It is now evident that if permanent interruption of the circuit is to be accomplished, the rate of dielectric recovery, measured in gap voltage to break the gap deg/dt must be greater than the rate of voltage rise der/dt or mathematically expressed $deg/dt > der/dt$. Further, anything done to reduce the rate der/dt is enhancing the interrupting ability. This can be done by introducing a resistor in series with the arc of such magnitude that the oscillatory capacity current becomes aperiodic. This is obtained when the value of this series resistance $R \geqq 2\sqrt{L/C}$. With a resistor of this magnitude the interruption may take place as shown in Figure 5. In this arrangement the resistor is initially in parallel with part of the arc path. Now as the current decreases the arc voltage will increase and, the current flowing through the parallel resistor will increase proportionally to the increase in arc voltage. Finally so much of the current flows through the resistor that the parallel arc becomes unstable and is extinguished. The resistor is now in series with the remaining arc. Since this resistor is many times greater than the short circuit impedance, di/dt will decrease and the steepness of the recovery voltage will be independent of the circuit constants, hence a slowup of der/dt to a value less than that at which interruption may take place.

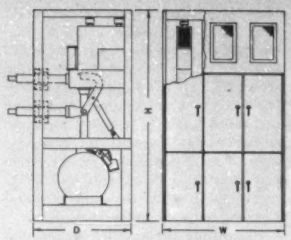
In Figure 5, the slope of the gap voltage has been exaggerated for illustration, but it is evident that by introduction of the resistor, not only the steepness of the gap voltage but also the actual magnitude has been considerably reduced.

Some recent investigations are tending to show that below a value of der/dt — 300–400 volts microsecond the air-blast circuit breaker is independent of the rate

Table I

Number of Shots Close-Open	Ampere, Rms	Volts, Rms	Cycles of Arcing
2	525	13,500	1/4 and 3/8
2	1,900	13,500	3/8 and 1/2
2	4,800	13,500	1/4 and 1/4
2	9,820	11,000	1/2 and 1/2
2	15,900	10,600	1/2 and 1/4
2	15,900	10,600	1/2 and 1/4
1	17,800	915	3/4*
2	28,600	1,100	1 and 1*
2	37,800	1,100	1/2 and 1/2*
2	16,700	1,058	0.2 and 0.4
2	26,700	1,070	0.4 and 0.1
2	35,100	1,083	0.5 and 0.2
2	55,200	1,100	0.5 and 0.4

* Without resistor.



MVA	AMP	D	H	W
500	1200	60"	123"	68"
1000	1200	60"	123"	68"
1000	2000	60"	123"	68"
1000	3000	60"	123"	68"
1500	2000	60"	130"	74"
1500	4000	60"	130"	81"
2500	5000	60"	138"	93"

Figure 6. Outline of type AB-15 air-blast circuit breakers

of recovery voltage rise, but above this figure interrupting ability rapidly decreases with the increase in recovery rate. This seems logical since for a certain pressure and structure configuration the rate of dielectric recovery may have a natural time constant, that is, a more or less fixed rate of dielectric recovery. It is then evident that any circuit voltage rise below this value will have no effect on interrupting ability. As this fixed value of dielectric recovery is approached, considerable reduction of interrupting energy may be required in order to retain the requirement for interruption mentioned previously. It is hence clear that improved interrupting ability will result as long as the resistance in series with the arc is low enough to effectively reduce the rate of arc re-establishing voltage.

Figure 6 shows the experimental circuit breaker and tabulation of dimensions.

Tests

Table I is a summary of preliminary tests on the interrupting device finally adopted for development as the most satisfactory from performance, maintenance, and economic considerations:

All tests were carried out at 150 pounds per square inch tank pressure in order to compare all devices at the same pressure level, and the tests were single phase, and except as marked, with a 12-ohm resistor. Figures 7 and 8 show typical oscillograms obtained during these tests.

Conclusion

We have briefly surveyed the major and fundamental engineering problems

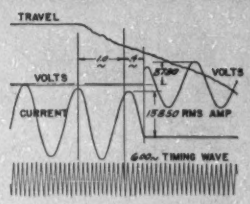


Figure 7. Oscillogram of single-phase close-open interruption of 15,800 amperes

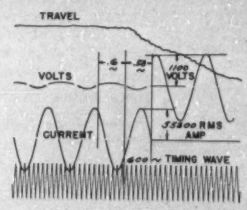


Figure 8. Oscillogram of single-phase close-open interruption of 55,000 amperes

in the development and design of high power air-blast circuit breakers. We have endeavored to at least indicate possible ways of solution and to give a better understanding of the factors affecting air-blast circuit breaker performance.

It is true that we have not yet reached ideal solutions for a good many of the problems encountered, and large scale research on all major phases is still being vigorously pursued with this as a goal. This specialized research, backed by long experience of American circuit breaker manufacturers in building oil and air circuit breakers, which successfully handle the greatest power concentrations in the world, will insure an improvement in circuit breaker performance, both technically and economically, to the mutual benefit of both users and manufacturers.

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Discussion

Robert H. Nau (University of Illinois, Urbana, Ill.): I am particularly interested in this paper which describes another version of a high-power compressed-air circuit breaker. My interest is enhanced more when I observe that the circuit breaker still stands, after considerable laboratory testing, very much as it existed on paper in 1946 when I was collaborating as design and development engineer for Allis-Chalmers Manufacturing Company with Jansson.

The hook-contact, which is actuated by a high-speed pneumatic operator, the direct-flow pipe from the air tank to the arcing region, and the single air tank, which provides a foundation for the circuit breaker proper, have proved again to be a very desirable arrangement for a high-power, indoor, cubicle-enclosed, air circuit breaker.

However, the test data which have been presented do not warrant rating the circuit breaker for more than 500,000 kva. It appears that the tests were limited by the maximum short-circuit capacity of the Allis-Chalmers High-Power Testing Laboratory. In which case, it has then been demonstrated that the circuit breaker is ample for the 500,000 kva interrupting capacity, but not necessarily for more. The fact that the circuit breaker has successfully opened approximately 55,000 amperes rms at the low voltage of 1,100 volts, and that other axial-blast circuit breakers have lent themselves well to high-voltage insulation in the form of outdoor circuit breakers does not provide any basis for extrapolating the kilovolt-ampere rating to correspond to full recovery voltage on a single-pole unit following an interruption of 55,000 amperes rms. My analysis of the present data, of other test data on both axial-blast and cross-blast circuit breakers, and of the air-flow and arc relationships in air-blast circuit breakers does not suggest very strongly that the axial-blast interrupter can be adapted to the pneumatic circuit breaker proper, with any amount of adroit dimensioning and contouring, for an interrupting capacity of 1,500,000 kva.

B. P. Baker (Westinghouse Electric Corporation, East Pittsburgh, Pa.): The author appears to be in the midst of making the very important decision—to determine what kind of a compressed air circuit breaker he is going to develop for heavy-duty indoor powerhouse service. It is gratifying and reassuring to know that a man with Jansson's broad experience and personal contact with European practice for a number of years, is now confirming the decision we made ten years ago. It is sincerely hoped that the next years will not prove that we were both in error.

I agree entirely with Jansson's conclusions regarding the general circuit breaker construction, type of contacts, type of interrupter, and operating pressure of 150 pounds per square inch. During the last nine years, we have delivered over 600 circuit breakers built along these lines, 40 per cent of which have been for 1,500,000 and 2,500,000 kva service. These circuit breakers have given an excellent account of themselves. Two illustrations are shown in the publications later referred to,[1,2] giving the general construction of these circuit breakers. The reservoir, mechanism, and associated mechanical parts are located in the lower compartment; the contacts, interrupter, and all live parts are located above. This arrangement has worked out quite well to give a straight simple air flow from the reservoir through the blast valves, interrupters, coolers, and into the gas receiving chamber at the top. These features together with a properly designed interrupter have made the interruption of 2,500,000 kva possible with air at 120 to 150 pounds per square inch, without the use of a shunting resistance. The contacts are of the finger and bayonet type, and as Jansson points out, the moving contact is completely withdrawn from the interrupter when the circuit breaker is in the open position. All contacts are, therefore, accessible for inspection and have been found adequate for continuous current carrying capacity up to 5,000 amperes. Previous publications[1,2] describe the circuit breaker and give interrupting test data up to 105,000 amperes rms at 13.2 kv, 2,000 volts per microsecond at 125 pounds per square inch without the use of a resistance shunt. It seems unfortunate that the author should delete these references from his references, especially after having followed their teachings in such great detail.

According to the teachings of the articles shown in my references referred to above, the circuit breaker ratings shown in Jansson's Figure 6 should be possible; and established American practice agrees with the dimensions shown. However, direct extrapolation from the author's best data—55,000 amperes at 1,100 volts, Figure 8, to the ratings and dimensions shown in Figure 6, seems entirely out of the range of engineering estimating.

I believe I was one of Jansson's contemporary investigators, whose opinion he did not share, "that an upper ceiling existed for the energy that could be interrupted by the nozzle type of interrupting element." While I admit that this statement should have been more quantitative, I still feel that for a given voltage rate of rise of restored voltage and air pressure, the unshunted nozzle has a lower current ceiling than a splitter type of interrupter.

However, as the rate of rise of voltage is decreased and the air pressure increased, the current ceiling is raised. This process continues on down below the 300 volts per microsecond referred to, even down to where the restored voltage is comparable to the sinusoidal 60-cycle wave. This theory, if carried too far may lead to impractical shunts which would place too much burden on the break interrupting the residual current. We have by use of shunting resistors, interrupted 65,000 amperes at 22 kv and 40,000 amperes at 44 kv on a single nozzle at 220 pounds per square inch; the residual current being interrupted by a second nozzle. At lower voltage, 15 kv and high values of current, this solution appears less practical than the one using the splitters and cross-blast which Jansson seems to have selected.

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Flashovers of Suspension Insulators Due to Contamination

H. A. ADLER
ASSOCIATE AIEE

W. H. WICKHAM
MEMBER AIEE

M. S. OLDACRE
FELLOW AIEE

Synopsis: Flashover of suspension insulators on steel tower lines, traceable to an accumulation of dirt followed by a deposit of moisture, has been the source of numerous outages of high-voltage transmission lines. The aim of the investigation described in this paper was to determine means of increasing the insulation level of existing lines under fog and dirt conditions. Methods were developed to simulate, in the laboratory, the conditions of dirt and fog which cause flashover of insulators in the field. Using these methods, a study was made of the phenomena of flashover of contaminated suspension insulators. The benefits to be derived from lengthened strings and the superiority of special fog-type units with increased leakage distance were determined. Other means of reducing the hazard from dirt accumulation are suggested.

Operating Experience

IN the Chicago area on the system with which the authors are associated, the source of contamination of insulators is industrial dirt. The seasons in which flashover troubles are most likely to be encountered are late winter and early spring. During the winter, the insulators receive little, if any, benefit from rain washing so that dirt accumulates. Following this exposure is the season when fog conditions most frequently prevail and flashover is most likely to occur. Operating experience has, in general, followed this pattern. A series of insulator flashovers occurred in 1944 and 1945 at several locations, on 132-kv and 220-kv overhead lines in the Chicago area. These troubles had the following characteristics in common:

1. They occurred in locations where insulators were exposed to contamination such as industrial dirt, fly ash, and railroad smoke.

2. They occurred usually after prolonged dry periods during which the contaminants could accumulate on insulators. However, there were cases when insulator troubles occurred after a very short time of exposure to contamination.

3. Flashovers occurred always during periods of fog or light drizzle which made the deposits on the insulators conductive.

The contaminants usually can be removed by washing of the insulators. However, this procedure is expensive so that frequent washing is undesirable. A continuous testing program is necessary to determine the need of washing.

Aim of Investigation

After a study of all information available from service experience and an extensive survey of the large amount of published information on this subject, the investigation was directed towards reinforcing the insulation strength of the suspension strings, under conditions of contamination, to such an extent that the insulators could withstand prolonged periods of contamination without trouble. It was expected that with added insulation strength, it would be possible to establish a cleaning schedule, similar to the overhauling schedule for other equipment, providing for washing of insulators only once a year. Such a procedure would not only reduce the frequency of washing, but also the washing could be scheduled at a convenient time, and under favorable weather conditions, thereby further reducing maintenance expenses. Under the conditions encountered in the Chicago area it is not considered practical, from an engineering and economic standpoint, to attempt complete elimination of insulator washing.

Test Methods

In order to compare the effectiveness of various methods of reinforcing the insulation strength of suspension strings under conditions of contamination, it was necessary to develop a method of artificial contamination and fogging. Studies in the field would have required an undue amount of time and would have been complicated by the many uncontrollable variables.

A new procedure of artificial contamination was developed which fulfilled the following three basic requirements.

1. Contamination producible in the laboratory in a short time.

2. Results closely reproducible.

3. Reasonable duplication of service contamination in type and pattern, and in its electrical effects.

Briefly, the method consists in depositing on each insulator a mixture of 75 per cent, by weight, of fly ash and 25 per cent Portland cement under a carefully controlled procedure of alternate fogging by means of steam and dusting through specially designed nozzles to produce a pattern of contamination similar to service conditions.

A weighed amount of the fly ash and cement mixture sufficient to produce the desired contamination was placed in a dust gun operated by controlled air pressure. Dust from this gun was blown against the insulator surfaces through two nozzles, one directed vertically upward against the lower surface of the insulator and the other directed horizontally against the edge. During the

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H. A. ADLER and W. H. WICKHAM are senior engineers with the Commonwealth Edison Company, Chicago, Ill., and M. S. OLDACRE is director of research, Utilities Research Commission, Chicago, Ill.

The authors wish to acknowledge the work of E. Schmitt, F. T. Carver and R. F. Freeman in conducting the large number of tests required for this investigation.

The apparatus for producing the fog was designed and developed by R. Patzelt and F. T. Carver.

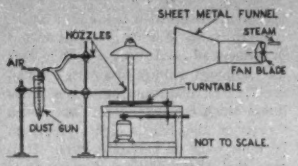

Figure 1. Equipment for contamination and fogging of insulators

process of contamination the insulator was slowly rotated on its vertical axis and was energized at 10 kv. This voltage is of the same order of magnitude as the average potential drop across a unit in service. Alternate fogging and dusting for measured time intervals, first with the vertical nozzle in positions to contaminate the lower surface, then with both nozzles, produced the desired pattern and degree of contamination. The dust was deposited most heavily in the bottom of the grooves on the under side, and fairly uniformly over the top surface. This closely approximates the manner in which dirt has been observed to collect on insulators in service.

Fog conditions were reproduced by directing a stream of wet water vapor, supplied by low-pressure steam, against the insulators. The apparatus for fogging and contaminating is shown diagramatically in Figure 1.

By carefully controlling the methods of contamination, fogging, and flashover, results obtained in flashover voltage tests were reproducible within ±10 per cent in short strings and even closer in long strings. Surface resistances showed wider variations. This would be expected from theoretical considerations because a narrow ring of relatively clean or dry area around the surface of a dirty fog-wet insulator will increase the surface resistance markedly without appreciably affecting the flashover voltage. For this reason it is felt that flashover voltage is a better criterion of the degree of contamination than is the surface resistance. The degree of contamination was so selected that the fog flashover of the insulator was approximately 1.5 times the average potential drop across a unit in service. This amount of contamination was found by field experience to be dangerous.

It should be noted that in making flashover tests on fog-wet contaminated insulators it is essential to have a power source of adequate capacity. Flashover of insulators under such conditions is preceded by arcing over parts of the surface and momentary increases in current. The regulation of the source must be such that the voltage will be sustained during these current changes, otherwise the results will be erratic. Furthermore, with the application of voltage to the wet and dirty insulators some drying takes place. The voltage must be increased rapidly to flashover in order to minimize the effect of drying.

Increasing the Length of Suspension Strings

First, a study was made of the advantages to be gained by increasing the length of suspension strings. The opinions expressed with regard to the value of such a scheme were found to be contradictory. The opinion of those who believed that adding of extra insulators was of no value under dirt and fog conditions, is based on the fact that the moist dirt on account of its high conductivity upsets the potential distribution along the string. In general, it must be expected that contamination may be nonuniform along the string. Then a relatively clean insulator on account of the greater I^2R losses will dry faster than the dirtier insulators. The clean and dried insulator will then take all or most of the voltage drop and initiate a flashover. According to this argument there will be no advantage in increasing the length of a string as there will always be some relatively clean insulator which will start the flashover of the string.

It was therefore necessary to include in this investigation the study of mixed strings of clean and dirty insulators. The most important phases of this investigation are shown in Figures 2 to 7. Figure 2 shows the comparatively slight reduction in flashover voltage under fog conditions when the insulators are clean and the great reduction when they are dirty. Figure 3 demonstrates that with regard to flashover voltages the artificial contamination produces results comparable to field conditions. Table I shows that with regard to surface resistivities, artificial contamination produced results of the same order of magnitude as field conditions.

Table I

Number of Insulators in String	Surface Resistance (Moist) Megohms Field Dirtied	Artificially Dirtied
6	0.49–0.66	0.65–0.80
18	1.3	1.5

In the study of the effect of clean insulators in strings of dirty insulators, a series of determinations were made of the potential distribution along the string. The right side of Figure 4 shows the potential distribution is not changed by fog if the insulators are clean and not changed by contamination if the insulators are dry. There is no measurable voltage drop across dirty and moist insulators and all the voltage is carried by the clean insulators under fog conditions.

As the next step in the study of mixed strings, Figure 5 shows the fog flashover voltages of a string of four with one to four clean insulators in the string. It can be seen that the minimum value occurs not for four, but for three dirty

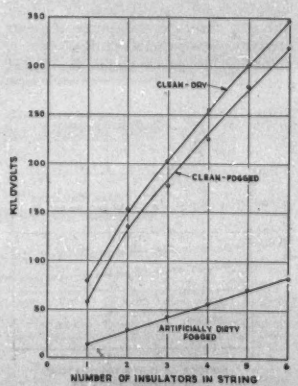

Figure 2. Flashover voltage of suspension insulators

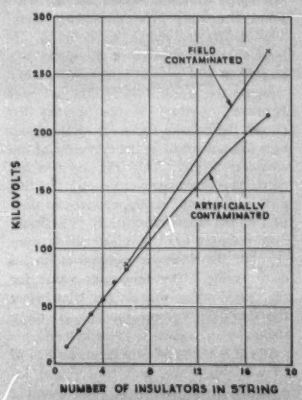

Figure 3. Fog flashover voltages for field-contaminated and artificially-contaminated insulators

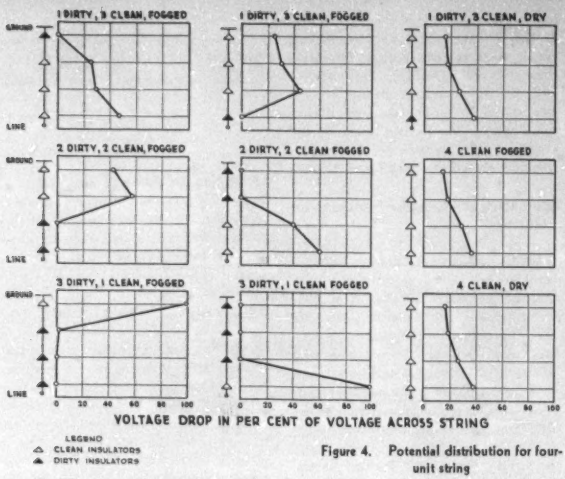

Figure 4. Potential distribution for four-unit string

insulators. This minimum value was about 11 per cent lower than the flashover voltage for a string with all dirty insulators. The figure shows two values in each case except for four insulators. The differences are caused by changes in the relative positions of the clean insulators in the string. The lower values were obtained in each case with the clean insulators on the line end.

In view of the observations on the effect of the location of clean insulators in the string upon the flashover voltages, studies were made on a string of six with one clean insulator in all possible positions, the other insulators being dirty. The results are shown in Figure 6. It is of special interest that the clean unit started to flash over at increasingly lower voltages the nearer it was to the line end. However, contrary to the opinion discussed before, the flashover of the clean unit did not result in the flashover of the complete string. This was the case because the flashover voltage of the five dirty units in the string was greater than that of the single clean unit. The flashover voltages for the mixed string were close to the flashover voltage of an all dirty string. The minimum value for the mixed string was six per cent below the flashover voltage for the all dirty string.

Finally, tests were made on strings of 18 with one, two, and three clean insulators on the line end, the remaining insulators being dirty. The minimum fog flashover voltage was obtained with three clean and 15 dirty insulators and was less than five per cent below the value for a string of 18 dirty insulators.

From these data it was concluded that the presence of clean insulators, especially in long strings, would not result in appreciably lower flashover voltages than for strings consisting of only dirty insulators. As a consequence it appeared justified to study the effect of adding insulators to a string using only dirty insulators without the time consuming study of mixed strings.

The lower curve of Figure 7 shows the results of fog-wet flashover tests on up to 24 dirty insulators. It can be seen that adding insulators does produce a definite increase in the fog-dirt flashover voltage although the rate of increase becomes somewhat less for longer strings.

On the Chicago 220-kv line the normal length of a suspension string is 18 units. An increase to 24 units produced an improvement of about 18 per cent in fog-dirt flashover voltage according to Figure 7. The maximum number of insulators which can be added to suspension strings on this line is eight. This can be done only on certain towers. It is estimated that addition of eight units will result in a 23 per cent improvement.

Fog Type Insulators With Increased Leakage Distance

Addition of eight units to suspension strings is not always possible on account of limitation with regard to clearances. For this reason studies were made on fog-type insulators designed for fog and dirt conditions on the Pacific coast. These insulators have nearly 50 per cent more leakage distance than the standard units. Figure 7 shows the superiority of these insulators over the standard insulators under fog-dirt conditions. On single circuit towers of the Chicago 220-kv line, 16 fog-type insulators can be installed with sufficient clearances. The data show that these 16 insulators give a much greater factor of safety than can be obtained with the 18 standard insulators normally installed or even with 24 standard insulators. No such advantage of fog-type insulators can be expected on dead end positions as in this case the large protective under surface of these insulators will be exposed to contamination. Therefore, such strings should be lengthened with standard units.

Reversing of Strings

At locations where the lower grooved surfaces of the dead end insulators face a source of contamination, the fog flashover of the insulators may be appreciably reduced. Since the larger portion of the leakage path is in this lower, grooved

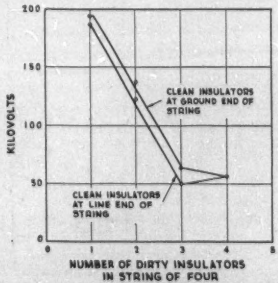

Figure 5. Fog flashover voltages for mixed string of four insulators

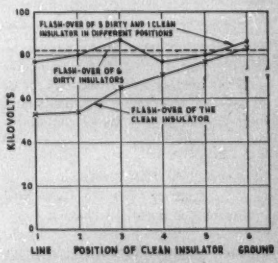

Figure 6. Fog flashover voltages for mixed string of six insulators

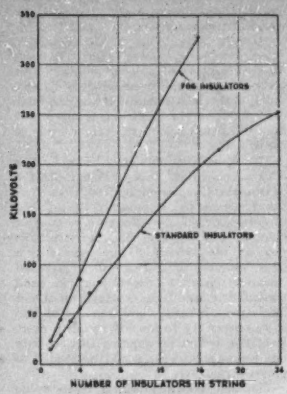

Figure 7. Fog flashover voltages for artificially-contaminated standard-type and fog-type insulators

surface, reversal of the insulators should be expected to result in an improvement by protecting these surfaces from unidirectional contamination. Test results shown in Table II for artificially contaminated 6-unit strings indicate that a considerable improvement can be obtained under these conditions by reversing the insulators.

Silicone Coated Insulators

Since silicone films are known to be very effective in repelling moisture, a series of tests were made on standard insulators covered with silicone. The idea was that dust would adhere less readily to a surface on which no moisture film could form. This phase of the investigation has not been completed. From the results at hand it would seem that some advantage can be obtained from such a coating if it can be made durable enough to withstand long exposure to the elements.

Conclusions

1. Increasing the length of strings of standard suspension insulators increases the flashover voltage under dirt and fog conditions.

2. Fog-type insulators in suspension strings are superior to standard insulators under fog and dirt conditions.

3. Installing dead end insulators with the smooth side towards the principal source of contamination results in improved fog flashover characteristics.

4. These schemes were put into effect at critical locations on the Chicago 220-kv line in the winter of 1946–1947. It is expected that by these measures the interval between insulator washing on this line can be one year or more, and that the washing can be scheduled at a convenient time similar to the scheduling of overhaul of other equipment. The result should be a considerable saving in maintenance expense. No washing has been done during the $1^1/_2$ years since the changes were made. At one especially critical location where all strings of standard insulators were lengthened, hand cleaning was done once when the line was out of service for other reasons. This was an added precaution, and possibly not necessary. Several years of experience will be needed to determine the permissible interval between washings.

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Table II

	Lower Surface Facing Source Of Contamination	Upper Surface Facing Source Of Contamination
Fog flashover, kv	112	178
Surface resistance of fogged string, megohms	1.8	8.5

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Discussion

P. L. Bellaschi (Consulting Engineer, Sharon, Penn.): Insulator contamination is a problem which confronts engineers in many lands. Although considerable progress has been made in recent years, the problems it presents have not been completely solved as yet. We are fortunate, therefore, in having the more recent findings as well as the general subject reviewed at this time.

In the course of an engineering trip throughout South America several years ago, this problem was brought to my attention repeatedly. Due to limited space, I shall confine myself here to a typical instance, with reference, specifically, to the difficulties encountered in the São Paulo region in 1944.

São Paulo lies inland some 50 miles from the sea on a plateau at about 2,000 feet (600 meters) elevation. The land rises abruptly from the seashore to the upper lands. Transmission lines (80 kv) from the hydroplants near sea level negotiate the bluff, then continue inland on the plateau to São Paulo. The lines are of steel tower construction and were insulated with four suspension insulators at the time. The operating voltage is 80 kv line-to-line (46 kv to ground). The dry flashover voltage being in the order of 250 kv, ratio between operating voltage and dry flashover is about 0.20.

Along the bluff, due to the warm humid air rising up to the plateau, the insulators are drenched year round and present no problem. On the plain, the rain comes at frequent intervals except for the dry season which lasts some three to four months—from May to August. During this period, the insulators of the lines on the plateau accumulate dust. The presence of new factories in the São Paulo region during the war further contributed in some measure to the contamination problem.

In 1944, the dry season had been unduly hot and the rains were fully a month overdue. Coupled with these conditions, this was the time (September–October) of heavy fogs. Starting in the evening, the fog would roll up the sea across the plateau and by early morning occasional flashovers would follow. For instance, on August 18, between 5:40 and 7:15 a.m., seven line outages were recorded. Under these conditions of combined dust, industrial contamination, and heavy fog, insulators that normally had a flashover strength of 250 kv would suffer a drastic reduction down to less than 20 per cent (46 kv, the operating voltage) with the result that copious flashovers would result at operating voltage. Under these adverse conditions, and in spite of some attempt at cleaning, there were a total of some 20 or more outages recorded in the course of a few weeks.

At the main hydrostation as well as at other generating stations, the neutral is solidly grounded but at the receiving stations (near São Paulo) neutrals are grounded through 100 ohms. The result of this was that when an insulator string would flashover, due to the overvoltages developed from the arcover propagated to the entire system, these overvoltages would induce flashover on other parts of the system because of a general lowering of the insulation throughout the system. For instance, at 6:40 a.m. on September 28, six of the lines tripped, at intervals possibly some 20 or 40 cycle and even more apart, according to the oscillographic records. During this period, a number of flashovers had occurred in the main receiving station.

In one instance, this station was de-energized in order to repair the damage. When all had been cleared and an attempt made to re-energize the bus, the insulation would not sustain the voltage. During the shutdown, the fog had completely wetted the insulation. Half an hour later, at 5:00 a.m., conditions apparently became more favorable and it was possible to return the station in service. Judging from the noise and pronounced sparking on the bus insulators, the leakage currents certainly must have been relatively high, gradually subsiding as the insulators dried out.

Under these circumstances, it was apparent that four suspension insulators resulted in a "tight situation." More liberal insulation was recommended. A cleaning and washing program of the insulators on the line and in the station was instituted.

First, I would like to ask the authors of the paper whether switching surges superimposed on the sustained 60-cycle operating voltage might not have precipitated some of the flashovers experienced on their system. If such should be the case, then a long sustained 60-cycle voltage with switching overvoltages superimposed thereon might well be a test of significance to give consideration in investigations of this character. In any event, it would seem that the 60-cycle test voltage should be sustained for a reasonably long period of time.

Second, have the authors observed either by cathode-ray oscillograph records or otherwise abrupt leakage current discharges preceding and possibly precipitating flashover?

And third, I would like to ask the authors whether in their view cleaning of the insulators under conditions of severe contamination as discussed here might not be predicated equally as much by radio-noise considerations in addition to the potential flashover hazard. Have radio-noise tests been conducted in the field and in the laboratory under these conditions?

The authors might find of pertinent interest the various contributions by W. Wanger presented in France in 1939 and later published in Switzerland.[1,2] Perhaps he was the first to investigate the relative effect of voltage duration on the flashover of contaminated insulators. Investigations conducted by the writer on insulators subjected to chilling in dry ice (CO_2) and then exposed to a humid atmosphere, and subjected, subsequently, to impulse, switching, and 60-cycle voltages confirm the work by Wanger generally well. The 60-cycle strength can well be reduced to 20 per cent of the dry strength, especially if the porcelain surface is contaminated. The impulse strength is not affected as much. The switching-surge strength is intermediate, the amount of the insulation strength is lowered depending on the duration. Dew and fog in combination with contamination in general present the worst condition.

In view of the increasing importance of the problem and the fact that we have not yet completely solved it, it would be well for the Transmission and Distribution Committee to continue giving its attention to this matter.

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M. S. Oldacre: This paper, together with three others on the same subject[1,3,2] cover some of the many phases of the insulator contamination problem and it is hoped that they will aid in promoting further study of the subject.

The author's paper should be read along with a companion paper "Pole Fires due to Insulator Contamination"[1] to fully understand how one user approached the problem of reducing their troubles to a reasonable amount, utilizing the available insulators now on the market. Thus there was a large element of the so-called practical research applied to the problem in hand without attempting the development of new insulators. A definite problem existed, and it was necessary to know how much over-insulation would be required so that only one washing per year would be needed to prevent trouble.

The first necessity was a reproducible method of contaminating the surface of the insulators so that the results of successive tests would be comparable. A study of published information indicates a wide variety of test methods have been tried with a varied degree of success and failure. Methods with salt solutions applied to the insulator surface seemed erratic in results and were abandoned in favor of the fly ash and cement mixture which is described in the paper.

Some other authors have mentioned that the presence of water soluble compound is necessary for low-leakage resistance. It soon became apparent in our tests that completely water-soluble materials were partially washed off as soon as the fog condensed on the insulator, and erratic results were obtained. Less soluble material which retained the moisture and formed a conducting surface without being easily washed off was more successful in obtaining reproducible results.

While the test described by the authors is probably not representative of all kinds of contamination, it seemed to serve its purpose of being a reproducible test for laboratory comparisons of different insulators and various lengths of strings. The fog-wet flashover of the artificially contaminated insulators was not too far from that of naturally dirtied insulators, hence should serve as a good yardstick of performance under various conditions.

The papers by Frey[2] and Taylor[3] point out the need for further development in insulators to help solve some of these contamination and fog problems, and it is believed that one of the most important elements is further development of test methods so that the benefit of changes in design can be determined by laboratory tests that will really represent field conditions.

It would appear that the semiconducting glaze might be a major improvement if the manufacturing problems can be controlled. Reports from England indicate that insulators with such glaze have been satisfactory for cable terminals and apparatus, but little is known of the difficulties of obtaining such insulators.

In answer to Bellaschi's questions, records show that none of the flashovers of con-

taminated insulators occurred during switching operations. No records were obtained of leakage current discharges preceding flashover. There has been no indication that contaminated insulators were a source of undue radio noise. Earlier tests in the laboratory and field of the effect of discharges in causing radio noise showed rapid attenuation with increasing distance from the line.

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Performance of Dust-Contaminated Insulators in Fog

BRADLEY COZZENS
MEMBER AIEE

T. M. BLAKESLEE
MEMBER AIEE

Synopsis: This paper presents a discussion of the problem of line insulator contamination by dust and the subsequent performance of the insulators in fog-laden atmospheres. A pioneer investigation of the subject carried out by the Department of Water and Power at Ryan High-Voltage Laboratory of Stanford University is described. Although the investigation was conducted in 1929, the information gained is still recognized as pertinent to the operation of transmission lines today. The quest for a trouble-free insulator for dust and fog areas still goes on while, in the meantime, periodic high-pressure water-stream washing of insulators, which followed the 1929 study of the problem and now is quite common practice, is an essential maintenance function to present operation.

THE detailed investigation of line-insulator performance described in this paper was prompted by experience with a 115-kv transmission line. A section of this line traversed a highly developed residential area. Audible noise and some radio noise which resulted from insulator sparking in fog-laden atmosphere brought complaints from residents of the area. This sparking in fog frequently resulted in flashovers with consequent line outages.

Published information on the subject was limited. Such data as were available in 1929 presented little discussion on the actual mechanism of dust deposition or on the arc formation that resulted from fog. It was felt that a thorough study of the problem might result in a better understanding of the situation and might possibly suggest an insulator design which would approach a solution to the problem. The data which were collected as a result of the investigation, though no longer new, still are fundamental and basic to the problem which currently is combated by the frequent washing of insulators.

Prior to 1929, the voltage distribution across insulator strings under dry conditions was considered of basic importance. Flashovers in fog were explained by water accumulating in large quantities on the insulators themselves and subsequently dripping past the units at the line end to initiate the arc. In the beginning of this study, close-range observations of insulators on transmission lines operating in fog-laden atmospheres were made which resulted in a somewhat different idea of the actual mechanism of flashover.

It was found that only one or two of the insulator units in a 7- or 9-unit string showed areas of visible corona or sparking, and at times these areas actually flashed over with limited-current arcs. These areas were located usually between the pin and the first petticoat of the insulator and the units were frequently, though not always, those near the line end of the string. Field observations made on these same insulator strings, operating in fog following a rain, revealed a somewhat different performance. Corona sometimes formed across areas on the top or edge of the insulator units in addition to the corona or sparking across areas on the under side of the units.

Preliminary attempts to study the problem in the laboratory by treating dust-coated insulators with fine water spray or with water vapor produced by boiling water in a small cabinet were made. It was evident, however, that more elaborate tests would have to be conducted to realize significant results and this led to the investigation described in this paper.

The Test Tunnel

Air movements existing at elevations of insulator strings on transmission towers in metropolitan areas carry turbulence; so, the tunnel used in the tests was not streamlined to produce uniformly smooth air flow. The test tunnel is shown in Figure 1. The centrifugal blower was capable of moving 140,000 cubic feet per minute. Six feet from the 5-foot 7-inch by 7-foot 4-inch fan outlet, the tunnel expanded to an 8-foot-square section. From this section to the test chamber, a distance of 20 feet, the sides of the tunnel were capable of giving an output opening adjustable to a maximum of 15 feet square. The final arrangement for the tests used an opening 15 feet high and 9 feet wide. Maximum wind velocity of 20 miles per hour was obtained through the test section with this size opening at the output of the tunnel.

Wind velocities below the maximum were obtained by restricting the air inlet into the fan. Vertical and horizontal vanes at the throat of the tunnel enabled some control of the uniformity of air flow across the test chamber. Synthetic dust flowed through a worm feed from a hopper into a $2^1/_2$-inch tube which opened in a vertical slot, 5 feet long, in the throat of the test tunnel. The dust flow from hopper through the tube and thence into the air stream was assisted by a small blower.

Dust

Since the immediate problem of the investigation was the study of the performance of the Department's (the Los Angeles Department of Water and Power) transmission lines, samples of dust which had

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BRADLEY COZZENS and T. M. BLAKESLEE are both with the Department of Water and Power, Los Angeles, Calif.

The authors wish to acknowledge and express their appreciation for the assistance of Doctor J. S. Carroll and other members of the staff of Ryan High-Voltage Laboratory of Stanford University.

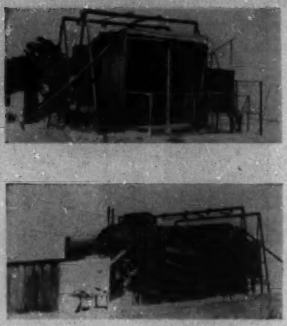

Figure 1. The test tunnel

accumulated on the insulators were obtained from two sections of the 115-kv transmission line to establish a basis for synthetic dust to be used in the experiments. The analyses of these two samples are presented in Table I. The sample from a section of the line adjacent to the railroad and to the Los Angeles River contained appreciable sulphur, petroleum products and carbon; the sample from a section of line in the San Fernando Valley, an agricultural area, was rich in organic materials and soluble salts. The make-up of the synthetic dust used in the tests is also shown in Table I.

Sodium, potassium, and calcium salts are the principal ingredients which contribute to the conducting properties of the dust layer when it is moist with fog. The synthetic dust was adjusted so that the conductance of an aqueous solution of the material was comparable to the same concentration of solution made with dust obtained from the line insulators.

It developed from the investigation, however, that aqueous solutions made from that part of the synthetic dust which accumulated on the test insulators carried slightly higher conductivity than the bulk material. This partially results from a mechanical and electrical separation occurring in the deposition process. Furthermore, difficulty was experienced as a result of the deliquescent property of some of the synthetic dust ingredients making it difficult to maintain the desirable dry, flocculent nature. It was necessary to heat the dust and maintain the admitting hopper warm in order to prevent compacting and clogging. For future tests, it is recommended that a dust composed of 50 per cent clay and 50 per cent feldspar plus sufficient powdered sodium chloride to produce the required conductivity be used.

Fog

Natural fogs are usually the result of condensation of water vapor around microscopic dust particles in the air and therefore may exist with humidities below the saturation point. With clean air it is possible to exceed 100 per cent relative humidity without the precipitation of fog.[1,2] In general, where hygroscopic particles are present in the air, fog is formed with 100 per cent relative humidity or at lower saturations.

The fog generator used in the tests was made up of a duplex high-pressure pump, an electrically heated boiler, and a series of nozzles. Distilled water from the pump was carried through the boiler where the temperature was raised to between 400 and 500 degrees Fahrenheit. It was then admitted into the lower section of the throat of the wind tunnel through a series of nozzles which produced a fine spray. The heat of the liquid at this pressure was sufficient to vaporize a large portion of the water coming out of the nozzles, thereby raising the relative humidity of the proximate air. The remaining water was broken up into finely divided particles by the swirling action of the nozzles to produce the artificial fog. So completely was the heat of the liquid used up, that the fog so produced was approximately at the temperature of the surrounding air by the time it was three feet from the nozzles.

The low day-time relative humidities of the Palo Alto climate made it necessary to conduct the fog tests at night to avoid evaporation of the artificial fog as it drifted down the tunnel to the test chamber. At night, with relative humidities of approximately 80 per cent, the fog generated was quite satisfactory. A slight opening in the intake of the fan was usually sufficient to cause a drift of fog from the nozzles across the insulators in the test chamber; the fan was not operated for fog application.

Measurement of Leakage Current

Leakage current over the insulator string was of particular interest as a measure of contamination and likelihood of arcover. The cable supporting the test insulator string was fitted with a special insulator above and outside the test chamber in such a position that it was not easily contaminated by dust or wetted with artificial fog. This permitted the leakage current over the test string to be carried to ground through a magnetic oscillograph element which recorded the current on 35-millimeter film. The current was recorded continuously from the time the fog was applied until some minutes after it dispersed and spitting and arcing over the insulator string subsided. Figure 2 shows a sample of film record.

A method of coding information on the film was employed using a second oscillograph element. It was thus possible for the observer to indicate which unit in the string was arcing, the severity of arcing, and other pertinent information, and thereby obtain a correlation between observed data and leakage current. Also other deflections of the coding element were applied automatically by a synchronous clock to indicate the duration of test.

Test Procedure

Considerable work was performed initially in the effort to establish a test procedure of dust application that would duplicate closely conditions observed on transmission lines. It was soon discovered that the pattern of the dust deposit is related to wind velocity. Dust blown at low velocity precipitates largely on the tops of the insulators while high-velocity wind deposits more dust on the under sides of the units.

Table I. Analysis of Insulator Dust

		Per Cent
Dust from 115-kv Transmission-Line Insulators Adjacent to Railroad Right of Way		
Loss on ignition—volatile matter		34.23
Made up of	Sulphur	2.57
	Petroleum ether extract (oils)	3.11
	Carbon and organic material	28.55
Mineral ash and insoluble silicates		65.77
Made up of	Iron oxide (Fe_2O_3)	10.04
	Aluminum oxide (Al_2O_3)	4.52
	Zinc oxide	2.61
	Calcium	trace
	Sodium and potassium	trace
	Kaolin and feldspar sands	48.50
Dust from 115-kv Transmission-Line Insulators in San Fernando Valley, Sandy-Loam Area*		
Loss on ignition—volatile matter		21.13
Made up of	Sulphur	1.91
	Moisture	2.00
	Volatile combined water-soluble acid radicals	6.26
	Sulphates	3.28
	Organic matter	7.46
Mineral ash and insoluble silicates		78.87
Made up of	Iron oxide (Fe_2O_3)	4.92
	Aluminum oxide (Al_2O_3)	5.12
	Zinc oxide (ZnO)	1.83
	Calcium oxide (CaO)	1.11
	Sodium oxide (Na_2O)	5.01
	Potassium oxide (K_2O)	4.28
	Kaolin and feldspar sands	56.58
Synthetic Dust Used in the Test Procedure		
English clay		34.0
Feldspar		33.3
Sodium potassium tartrate		11.6
Aluminum hydrate		6.1
Iron oxide (red Spanish Fe_2O_3)		5.9
Calcium carbonate		1.1
Zinc oxide		1.8
Sodium sulphate		6.2

* Material contained 17.02 per cent water-soluble matter consisting of sulphates (SO_4), chlorides (Cl), carbonates (CO_3), and organic acids.

The preliminary studies showed that proper deposition of dust could be obtained only with the insulators subjected to normal operating voltage while the dust was being blown by. The deposits of dust under energized conditions were concentrated around the pin; without energization the deposit was more uniformly distributed on the under sides of the insulators.

Laboratory observations of dust dropped between plates carrying an alternating potential difference show that many dust particles blown into the air carry electrical charges. Such charged dust particles passing an energized insulator string may be attracted and deposited upon, or adjacent to, the pin or cap of the insulator. Insulator pins operate frequently with a small amount of corona especially when in a moderately high-velocity wind stream. The space charge produced by this corona travels out at high speed. Thus, dust traveling at 20 miles per hour (4 inches per half cycle at 60 cycles per second) may receive a charge of one polarity while three or four inches in space from the insulator pin. On the succeeding half cycle, with reversal of charge on the pin, the dust particle is attracted to and may deposit about the pin to give the characteristic dust pattern found on energized insulator units in service.

Dust applications were made with wind moving at 20 miles per hour and also at 3 miles per hour; the latter wind velocity was used to produce a deposit of dust on the tops of the insulators. The test procedure adopted resulted in the depositing of a little more dust on the top surfaces of the insulators than is usually found in practice. However, since the top surfaces are easily cleaned by rain, and, since, in the test setup, this top surface dust was easily leached by the artificial fog, this difference in test conditions from practice was not considered serious.

The standard string consisted of nine units where the pitch was $5^3/_4$ inches or less; for units with larger pitch, the string lengths were maintained between 45 and 50 inches. All strings were energized with 76.4 kv to ground, in order to subject them to the normal operating voltage of the Department's 138-kv system.

The standard test for the various insulator strings covered by the investigation consisted of five dust and fog periods as outlined below:

First Period

Three pounds of dust blown through the tunnel at 20 miles per hour.

Two pounds of dust blown through the tunnel at three miles per hour.

Five minutes of fog.

Ten minutes of wind at 20 miles per hour to dry units.

Second Period

Units given the same treatment as first period.

Third Period

Units given the same treatment as first period.

Fourth Period

Five pounds of dust blown through the tunnel at 20 miles per hour.

Three pounds of dust blown through the tunnel at three miles per hour.

Ten minutes of fog.

Ten minutes of wind at 20 miles per hour to dry units.

Fifth Period

Five pounds of dust blown through the tunnel at 20 miles per hour.

Three pounds of dust blown through the tunnel at three miles per hour.

Forty minutes of fog with the last ten minutes of this fog period made exceptionally heavy.

A collection was made of practically all the standard shapes of suspension-type insulators available at the time, and, in addition, experimental insulators were made from twelve special patterns. The patterns included five modifications of the standard unit, which eliminated successively the petticoats progressing from the pin to the outer edge of the unit, and 10-inch diameter flat units with various numbers of very small petticoats or protrusions on the bottom surfaces. Another series of patterns varied the pin size from 1/2 inch to $1^1/_2$ inch and used a porcelain disk with a small bead on the outer rim. A string of insulators using old 12-inch diameter flat Thomas disks, assembled with modern cap-and-pin hardware, was made up in the laboratory and subjected to the dust and fog tests. Cross-section diagrams of most of the suspension units tested are shown in Figure 3.

Discussion of Test Results

Insulator strings subjected to the full test procedure, or sometimes only four cycles of dust and fog applications gave leakage currents varying from 50 to 100 milliamperes when in the fog cycle. Units of the conventional type flashed over usually before the leakage current reached 100 milliamperes. It was concluded that insulators in such a con-

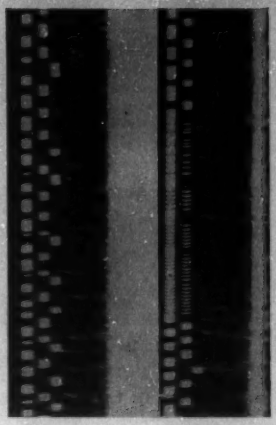

Figure 2. Samples of 35-millimeter film record of leakage current and coding trace

taminated condition and operating on actual transmission lines in fog will flashover when the leakage current reaches 50 to 60 milliamperes. In the limited test period of a few minutes, the various probabilities do not always combine to allow flashover to take place. It was very rare, however, that insulator leakage currents reached 100 milliamperes without resulting in flashover. Large insulators or insulators with deeply grooved skirts, however, generally carried higher currents without flashover than the smaller standard units.

The color of the sparking on insulators may be indicative of the performance of the string, although this fails sometimes because of the coloring caused by deposited salts. A blue sparking on the units generally indicates that the insulator string is performing satisfactorily. When the arcs across dry areas adjacent to the pins are deep yellowish or reddish, the current is probably in excess of 25 milliamperes, and the string is approaching the threshold of flashover.

The tests and observations of the performance of many line insulators through the cycles of dust and fog applications resulted in a good understanding of their performance under such conditions. The normal voltage distribution over a dry dust-covered insulator string that exists as a result of the electrostatic capacitances of the units is upset as soon as fog moistens the dust layer. When moist, the normal capacitance current of one or two milliamperes is masked by the leakage current as the moist layer of conducting

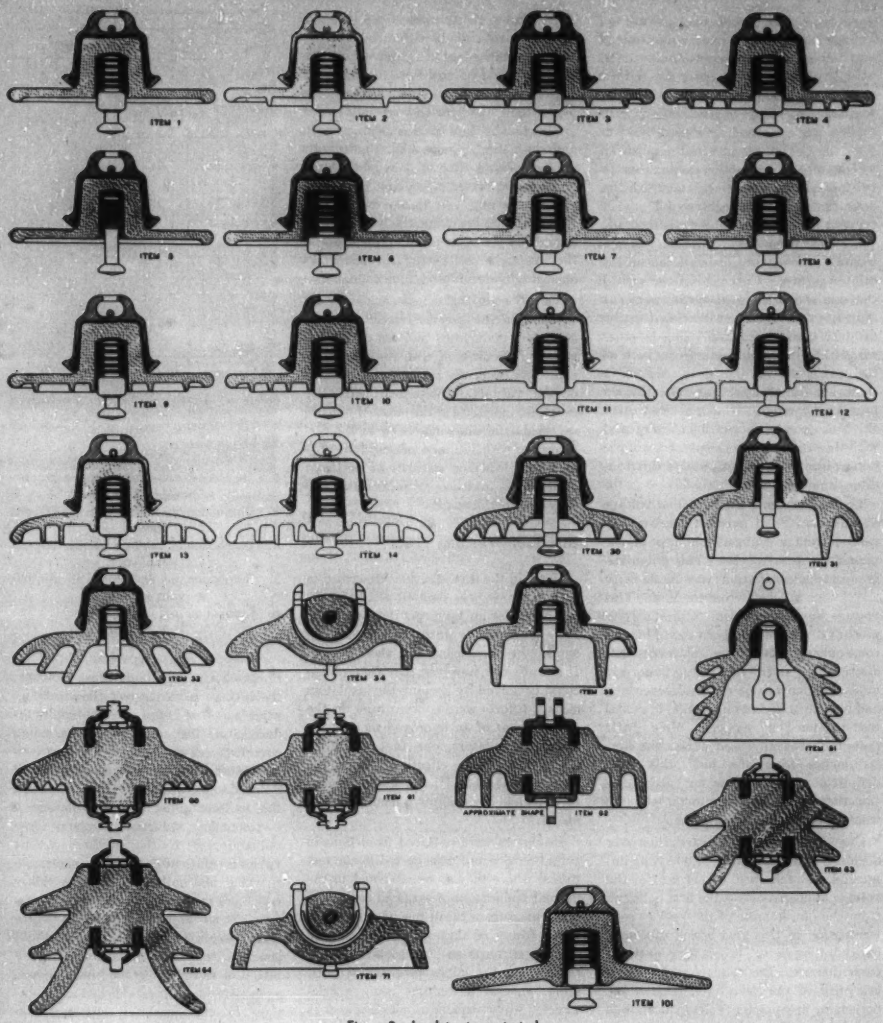

Figure 3. Insulator types tested

dust turns the insulator string into a series resistance from line to ground.

The resistances of the various sections of this series circuit may not be uniform over the entire insulator string, resulting in a nonuniform voltage and energy distribution. Even with a uniform dust coating and wetting, the increased current densities adjacent to the cap and pin will cause increased voltage drop and increased heating and drying in these areas. Even with heavier dust deposits adjacent to the pins and caps these areas usually carry the bulk of the voltage. Since the distance over these areas is usually small, the areas are highly stressed and break into corona or become evident by surface sparking. The sparking results from capacitance discharges, but arcs of higher current occur occasionally over these dry areas. The current is, of course, limited by the series elements of wet film on the insulators. The wet sections, which are carrying little or none of the applied voltage, appear dark in contrast with the dry corona-covered or sparking areas.

As a further proof of this uneven voltage distribution over dust-covered and moist suspension insulator units, actual voltage gradients were determined in this investigation with a high-voltage potentiometer. These determinations were made using a method developed by Doctor J. S. Carroll of Stanford University which employs a sensitive electroscope as the indicator of voltage balance with the potentiometer. The curves of Figure 4 prevent the results. Curve *1* is theoretical, calculated on the premise of a uniform coating and wetting over the entire insulator surface. Curve *2* is a theoretical curve in which the surface resistance per unit area is assumed proportional to the distance out from the pin. This is somewhat in line with the actual condition as the heavier deposit of dust near the pin (a condition that always obtains as explained previously) reduces the resistance over this area upon initial wetting as compared with the resistance at the outer edge of the porcelain. When drying starts this conditon is completely disturbed.

Curve *3* presents the measured voltage distribution found over an insulator removed from a transmission line in the vicinity of Los Angeles. This potential-gradient determination was made with 2,000 volts applied across the unit. This voltage was not sufficient to produce significant drying of the moist dust film and consequently the curve represents the distribution over relatively uniformly moistened surface of an insulator which had accumulated contamination in actual transmission-line service. Very little potential drop appeared across the first six inches from the pin; this gave definite proof of the heavy dust accumulation that occurs adjacent to the pin as a result of electrical precipitation.

Curve *5* represents the distribution over a similar unit taken with 10,000 volts impressed. In this case, 40 per cent of the voltage appeared across the first half inch from the pin, in spite of the heavier contamination in this area which was indicated by curve *3*. Progressing outward from this area, the voltage gradient was low until, at the clean outer edge of the porcelain, appreciable voltage drop was sustained. There was practically no voltage drop over the top surface except at the area immediately adjacent to the cap. The 10,000 volts impressed across this unit caused sufficient current to dry the areas adjacent to the pin and cap forcing these areas to carry large percentages of the total voltage.

Curves *4* and *6* show voltage distributions across an insulator unit which had been subjected to the first period of dust deposition in the test procedure; the distributions were made with 2,000 and 10,000 volts respectively to give comparative data for curves *3* and *5*. The similarity between curves *2* and *4* demonstrates the close approach of the test procedure dust application to the situation as it occurs in actual practice. Curve *6*, for 10,000 volts applied, shows that 97 per cent of the voltage was carried by the first $1^1/_2$ inches of insulator surface adjacent to the pin. This situation bears out the basic reasoning behind the explanation of the cause of the observed performance of insulators operating under dust and fog conditions.

Variations of dust consistency in the air stream and the general erratic action of insulators under dust and fog conditions make it difficult to give an absolute statement regarding the relative effectiveness of the various insulators tested. For example, the study of the effect of pin size revealed little difference between the performance with 1/2-inch pins and the $1^1/_2$-inch pins. Minor changes in petticoat dimensions produced no measurable difference in performance.

Insulator-Washing Tests

Early in the investigations described in this paper, it was realized that the development of an insulator that would be the answer to the dust and fog problem would not be possible in a short period. It was obvious, however, that the problem could be solved by keeping the insulators clean. Efforts were concentrated on the development of an improved method for cleaning insulators, one that would be cheap and still be effective. Thus, the 1929 tests at Ryan High-Voltage Laboratory included insulator-washing experiments.

Insulators were removed from lines in actual service and transported to the test tunnel where they were subjected to the normal application of artificial fog to obtain a measure of the degree of contamination. Many of these insulators showed leakage currents as high as 35 milliamperes. The insulators were then washed under various procedures using nozzle-directed water streams at nozzle pressures ranging from 200 to 500 pounds per square inch. The nozzle used had a 3/32-inch aperture.

It was not difficult to remove most of ths soluble materials from insulators contaminated with dust from sandy-loam agricultural areas, but it was not possible to clean completely the surfaces or to remove all soluble material from insulators contaminated with soot and smoke as a result of locations adjacent to railroad rights of way. In both cases, however, the leakage currents over the insulators when operated in fog were materially reduced from the values for corresponding circumstances when dirty. The dust-contaminated insulator leakage current was reduced to less than two milliamperes and the soot-contaminated units showed a corresponding reduction to about three milliamperes. Neither of these current values is sufficient to result in appreciable sparking, and both are well below values which are in danger of resulting in flashover. It was apparent that high-pressure water-stream washing of insulators would insure safe operation of the units for some period of time. A periodic maintenance operation was thus established as a solution to the insulator contamination problem.

There then followed the development of insulator-washing equipment to facilitate this maintenance operation. One of the insulator-washing trucks used by the Department for routine cleaning of pin-type and suspension insulators is shown in Figure 5.

The truck is equipped with water tank, high-pressure pump, and an 85-foot fire-type extension ladder. Pres-

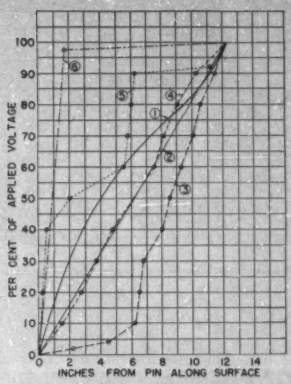

Figure 4. Voltage distribution over surface of suspension-type insulator unit

1. Theoretical curve based on uniform resistance per square inch
2. Theoretical curve for resistance proportional to distance
3. Distribution on unit from line—2,000 volts applied
4. Distribution on unit from test—2,000 volts applied
5. Distribution on unit from line—10,000 volts applied
6. Distribution on unit from test—10,000 volts applied

Figure 5. Insulator-washing truck in operation

ent practice is to use positive-displacement pumps with regulator valves producing pressures up to 1,000 pounds per square inch at the pumps and capable of delivering 50 gallons per minute. Long tapered nozzles are used with openings up to 1/4-inch diameter. Many improvements in nozzle design and construction are described in a paper by G. Leslie Hill.[3] The ladder on the truck permits raising the man and nozzle to sufficient elevations to properly wash insulators on the majority of 34.5-, 115-, and 138-kv circuits. The taller 287.5-kv towers are provided with piping from base to insulator levels where taps are provided for attaching a short section of hose with the directing nozzle. The high-pressure water output of the washing truck connects to the piping at the tower base.

Washing schedules were established after an extensive survey and check of conditions on transmission lines operating in and adjacent to Los Angeles. The 34.5-kv distribution circuits and the 115- and 138-kv transmission circuits are washed at three-month intervals and the 287.5-kv circuits are washed every six months in residential areas only.

Where duplicate circuits and loads permit, insulators are de-energized for washing. At first the washing of energized units was performed only on 34.5-kv circuits, but this practice was soon extended to all voltages including 287.5 kv. Before extending the operation to the higher voltages, however, extensive tests were conducted to determine the current through the nozzle stream under various water pressures and for various distances from the energized parts. This ground has also been thoroughly covered by Hill.[3] Washing is not carried on normally with distances less than 15 feet between nozzle and energized circuit.

Conclusions

The voltage distribution over contaminated insulators operating in fog is determined by the leakage current flowing over the insulator surfaces, and any normal capacitance-controlled voltage distribution is masked by this resistance-controlled voltage division. As a result of heat developed over sections of the insulator surface resistance, certain areas of the insulators dry rapidly increasing the resistance of such areas and forcing them to take large portions of the total voltage across the insulator string. The dry areas become overstressed and therefore exhibit corona, sparking, or occasionally flashover to give higher leakage currents that are limited only by the resistance offered by what damp sections of insulator surface remain.

Test procedures can be established which will produce results quite comparable with experiences in actual practice. The results of such tests have indicated little, if any, superiority in performance of the various existing conventional designs of insulator units. The 12-inch flat disk-type insulator shows a slight superiority, but the excessive corona and resultant radio disturbance and audible noise precludes its use in thickly populated areas.

The washing of insulators with clear, high-pressure water jets at regular intervals, established as required in accordance with the extent of dust accumulations and the frequency of fog occurrences, can result in overhead, high-voltage line operation free of flashovers from dust and fog. Observations of local conditions of the audible noise level and the nature of arcing on insulators can be used as criteria for determining when it is necessary to start insulator washing to prevent flashovers.

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3. Tests and Developments in Connection with Hot-Line Insulator Washing, G. Leslie Hill. AIEE Transactions, volume 66, 1947, pages 1203–16.

Discussion

P. L. Bellaschi (Consulting Engineer, Sharon, Pa.): Insulator contamination is a problem which confronts engineers in many lands. Although considerable progress has been made in recent years, the problems it presents have not been completely solved as yet. We are fortunate, therefore, in having the more recent findings, as well as the general subject, reviewed at this time.

In the course of an engineering trip throughout South America several years ago, this problem was brought to my attention repeatedly. Because of limited space, I shall confine myself here to a typical instance with reference, specifically, to the difficulties encountered in the São Paulo region in 1944.

São Paulo lies inland some 50 miles from the sea on a plateau at about 2,000 feet (600 meters) elevation. The land rises abruptly from the seashore to the upper lands. Transmission lines (80 kv) from the hydroplants near sea level negotiate the bluff, then continue inland on the plateau to São Paulo. The lines are of steel tower construction and were insulated with four suspension insulators at the time. The operating voltage is 80 kv line to line (46 kv to ground). The dry flashover voltage being in the order of 250 kv, ratio between operating voltage and dry flashover is about 0.20.

Along the bluff, due to the warm humid air rising up to the plateau, the insulators are drenched year round and present no problem. On the plain, the rain comes at frequent intervals except for the dry season which lasts some three to four months—from May to August. During this period, the insulators of the lines on the plateau accumulate dust. The presence of new factories in the São Paulo region during the war further contributed in some measure to the contamination problem.

In 1944, the dry season had been unduly hot, and the rains were fully a month overdue. Coupled with these conditions, this was the time (September-October) of heavy fogs. Starting in the evening, the fog would roll up from the sea across the plateau, and by early morning, occasional flashovers would follow. For instance, on August 18, between 5:40 and 7:15 a.m., seven line outages were recorded. Under these conditions of combined dust, industrial contamination, and heavy fog, insulators that normally had a flashover strength of 250 kv would suffer a drastic reduction down to less

than 20 per cent (46 kv, the operating voltage), with the result that copious flashovers would result at operating voltage. Under these adverse conditions, and in spite of some attempt at cleaning, there were a total of some 20 or more outages recorded in the course of a few weeks.

At the main hydrostation as well as at other generating stations, the neutral is solidly grounded but at the receiving stations (near São Paulo) neutrals are grounded through 100 ohms. The result of this was that when an insulator string would flashover, due to the overvoltages developed from the arcover propagated to the entire system, these overvoltages would induce flashover on other parts of the system because of a general lowering of the insulation throughout the system. For instance, at 6:40 a.m. on September 28, six of the lines tripped, at intervals possibly some twenty or forty cycles, and even more, apart, according to the oscillographic records. During this period, a number of flashovers had occurred in the main receiving station.

In one instance, this station was de-energized in order to repair the damage. When all had been cleared and an attempt made to re-energize the bus, the insulation would not sustain the voltage. During the shutdown, the fog had completely wetted the insulation. Half an hour later, at 5:00 a.m., conditions apparently became more favorable and it was possible to return the station in service. Judging from the noise and pronounced sparking on the bus insulators, the leakage currents certainly must have been relatively high, gradually subsiding as the insulators dried out.

Under these circumstances, it was apparent that four suspension insulators resulted in a "tight situation." More liberal insulation was recommended. A cleaning and washing program of the insulators on the line and in the station was instituted.

I would like to ask the authors of the paper whether switching surges superimposed on the sustained 60-cycle operating voltage might not have precipitated some of the flashovers experienced on their system. If such should be the case, then a long sustained 60-cycle voltage with switching overvoltages superimposed thereon might well be a test of significance to consider in investigations of this character. In any event, it would seem that the 60-cycle test voltage should be sustained for a reasonably long period of time.

Have the authors observed either by cathode-ray oscillograph records, or otherwise, abrupt leakage current discharges preceding and possibly precipitating flashover?

I would like to ask the authors whether, in their view, cleaning of the insulators under conditions of severe contamination as discussed here might not be predicated equally as much by radio-noise considerations in addition to the potential flashover hazard? Have radio-noise tests been conducted in the field and in the laboratory under these conditions?

The authors might find of pertinent interest the various contributions by W. Wanger presented at the International Conference on High Voltage Electric Systems in 1939 and later in Brown Boveri Review.[1,2] Perhaps he was the first to investigate the relative effect of voltage duration on the flashover of contaminated insulators. Investigations conducted by the writer on insulators subjected to chilling in dry ice (CO_2) and then exposed to a humid atmosphere, and subjected, subsequently, to impulse, switching, and 60-cycle voltages confirm the work by Wanger generally well. The 60-cycle strength can well be reduced to 20 per cent of the dry strength, especially if the porcelain surface is contaminated. The impulse strength is not affected as much. The switching-surge strength is intermediate; the amount the insulation strength is lowered depending on the duration. Dew and fog in combination with contamination in general present the worst condition.

In view of the increasing importance of the problem and the fact that we have not yet completely solved it, it would be well for the transmission and distribution committee to continue giving its attention to this matter.

References

1. The Flashover Voltage of Dirty and Wet Insulators Were Subjected to Impulses of Various Duration, **W. Wanger.** *Report Number 209*, International Conference on High Voltage Electric Systems, (Paris, France) 1939.

2. Flashover Voltage of Insulators and Spark Gaps in the Intermediate Range Between Impulse, and Voltage Testing at Operating Frequency, **W. Wanger, W. Huber.** *The Brown Boveri Review* (Baden, Switzerland), volume 27, December 1940.

Bradley Cozzens and T. M. Blakeslee: Bellaschi has discussed a condition existing in South America which is not entirely unlike that found in southern California and other west coast areas. Extended dry periods, where conducting dust is precipitated upon the insulator surfaces, either with or without alternate periods of condensation to cement the dust into place, followed by periods of heavy fog or mist appear to be the most serious in causing flashover.

The Boulder transmission line of the Department of Water and Power is washed regularly through the populated area, but, at a location approximately seven miles from the Los Angeles terminus, washing was not practiced for the first ten years of operation. A relatively long dry period, extending from March to September 1946, allowed heavy accumulations of dust to develop, so that when the first fog actually did occur, flashovers were frequent.

It was on this occasion that it became necessary to wash the 287-kv transmission circuit for an additional seven miles while the circuit was energized.

Replying specifically to the questions of Bellaschi, switching surges most certainly would tend to precipitate or initiate flashover of a contaminated insulator string operating in fog. Strings severely contaminated operate with but small areas of the insulator surface dry and carrying practically all of the line to ground voltage. The application of the surge would, of course, flashover these dry areas probably on many of the insulator strings, and, on some strings, the remaining surface resistance would not be sufficient to limit the follow-up arcing current to a sufficiently low value to prevent flashover.

Regarding the suggestion of this procedure as a method of test, such a procedure might lend considerable information to the art of testing insulators in fog, but, on the transmission lines, where several hundred insulator strings are involved, it is again a matter of probability as to which string will flashover, and laboratory testing on this basis would be relatively difficult, particularly from a statistical standpoint.

Regarding the second question as to observation with a cathode-ray oscillograph, such records have been obtained by other authors, but the authors of this paper have confined their measurements to normal magnetic oscillograph records. From the mechanism involved, however, it is practically certain that each flashover which occurs is the result of cascade action, namely, the flashover of one dry area throwing additional voltage on to the dry areas of other units in the insulator string with ultimate breakdown of the entire string.

Regarding the third question by Bellaschi as to whether or not radio noise might be a measure or criteria for determining the cleaning of insulators, it has been our observation that the radio-noise level depends very materially upon the degree of wetting of the insulator surface and to some extent upon the nature of the contamination. After wetting has continued for a period of hours, the insulators may become relatively quiet both audibly and electrically, and only an intermittent spit or buzz in a radio receiver will result. On the other hand, under certain very adverse conditions, dry areas may be formed immediately adjacent to the cap or the pin of the insulator, and practically continuous capacitance discharge exists between the edge of the wetted surface and the metal parts. When this exists, the radio noise is very evident and can be recorded for some distance from the tower upon which the disturbance occurs. It has been our observation, however, that this condition may occur when the maximum leakage current might still be limited to 10 or 15 milliamperes while a much more severely contaminated insulator might fail to develop radio noise. A study of the noise patterns developed as a result of various degrees of contamination might eventually yield a method of measurement as a basis upon which to determine when insulators should be washed.

Bellaschi's discussion again serves to indicate that the problem is not limited to isolated areas, but is relatively universal in its extent, as indicated in our paper.

Voltage Surges in Relay Control Circuits

T. R. HALMAN
MEMBER AIEE

L. K. HARRIS
MEMBER AIEE

THE SUPREME importance of protective relays is universally recognized and much engineering study is devoted to their design, manufacture, application, and maintenance. By comparison the battery and control circuits, being less spectacular and inherently less complex, may appear to require but superficial attention. Nevertheless great care is justified in the planning, installing, and maintaining of station controls to obtain the utmost reliability. The battery with its associated control elements actually occupies a position of major importance in a station, first because its failure would put the entire station control out of commission, and second because all of the station d-c control wires are brought together at its positive and negative terminals or at busses connected thereto, and through these, troubles can be communicated from one part of the station to another. It is common practice to separate and isolate the positive and negative busses, fuse branch circuits to disconnect them before local trouble can spread to the whole station control, and to ground the control system at one point only and then through lamps so that a ground on either the positive or negative side will not cause a short circuit on the battery, but will immediately give an indication or alarm. Control circuits are usually well isolated from the high-voltage lines, which they control, and are little influenced by potential transients due to lightning, high-voltage switching, or fault currents. They are, however, subjected to high-voltage surges which are caused by the discharge of energy stored in the magnetic fields of inductive devices which are connected to them. This paper deals with problems concerning these voltages and with methods for their control and suppression. The material and ideas which are presented have been gleaned to a large extent from experiences and operating practices of the company with which the authors are associated. The proposed methods of control and suppression may not be readily applicable to all schemes of d-c control, but it is believed that the ideas to be presented are sufficiently inclusive to merit general consideration.

Insulation stresses, due to the de-energizing of inductive circuits, well above the value deemed safe for periodic testing are often encountered. Voltages as high as 5 kv have been recorded during the discharge of small 125-volt relays and by paralleling the coil with a small capacitor (100–200 micromicrofarad), thus simulating the effect of the capacitance of a short length of control wire, voltages as high as 7 kv were measured. Lower surge voltages usually accompany the discharge of large devices such as the coils of the solenoids used to close large circuit breakers, however, the energy is always much greater. Transients in excess of 1,700 volts have been obtained from the de-energizing of 125-volt solenoid closing coils and 2,500 volts from 250-volt closing coils.

Uncontrolled voltages of this order of magnitude on the battery and control circuits of a station may result in injury to personnel, unnecessary service interruptions, and equipment damage. Any of the larger inductive devices such as circuit breaker closing solenoids, tractive and lifting magnets, and machine fields discharge sufficient energy and at proper voltage and duration to cause severe and possibly fatal electric shock. If a ground exists in the solenoid coil, the control contactor, or the intervening wire, or is caused by the discharge transient, it will expose the entire station control to a high voltage. Such a voltage surge, when imposed on the control wiring of a station, in seeking the easiest path to ground may break down the insulation of relays, current transformers, terminal blocks, and wiring and in discharging to ground, trip operating lines with consequent unwanted equipment outages and service interruptions. Equipment damage in itself is usually less serious than the other types of trouble. However, it is often difficult to locate, and until found and corrected, will remain a potential hazard which could become serious at any time. Repairs may lead to an extensive and expensive program of preventive maintenance in the form of repairing, altering, or replacing all pieces of equipment of a certain type which has proved vulnerable.

The elements of a typical control circuit are shown in Figure 1. When the contacts are closed, the current through the inductive circuit of the coil rises to a value determined by the voltage of the battery and the resistance of the entire circuit. The instantaneous current at any time t is determined from a solution of the differential equation of the circuit.

$$E = iR + L di/dt \qquad (1)$$

The solution of this equation is complicated by the fact that the inductance L in such an iron path is a function of both current and armature travel and that the resistance R is influenced by iron losses in the core material. A solution useful for approximate results is obtained by assuming the circuit parameters to be constant. This leads to the familiar equation

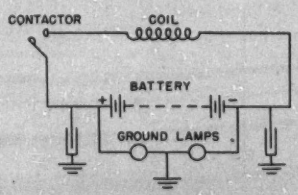

Figure 1. Schematic diagram of typical circuit breaker closing circuit

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T. R. HALMAN and L. K. HARRIS are both with The Detroit Edison Company, Detroit, Mich.

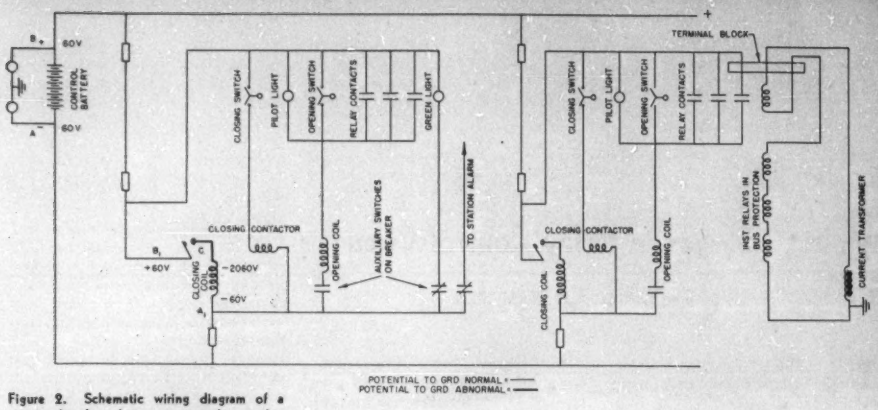


Figure 2. Schematic wiring diagram of a circuit breaker being operated, another similar circuit breaker, and instantaneous relay circuit of a bus protective scheme

Potential at A and A_1 is −60 volts. Potential at B and B_1 is 60 volts. Assume 2,000 volts induced in closing coil by collapsing flux. Potential at C is −2,060 volts. Voltage across contactor is 2,120 volts

$$i = E/R\left(1 - \epsilon^{-\frac{Rt}{L}}\right) \qquad (2)$$

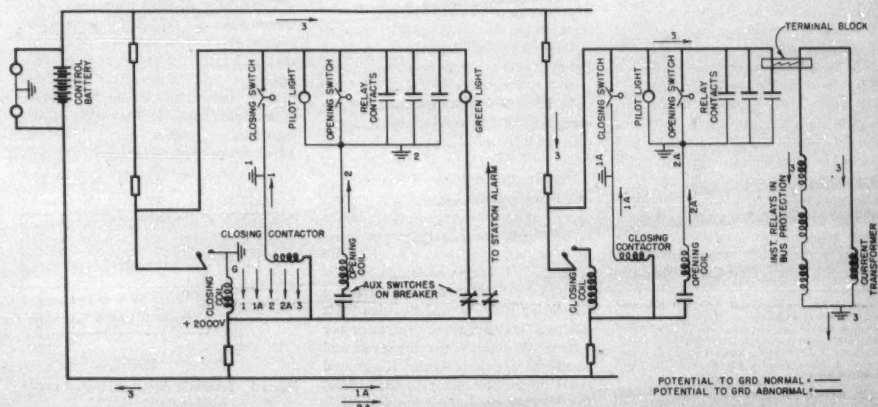
The time constant of relays is very short and even with the solenoids used to close large circuit breakers, the work cycle is completed and the current substantially constant in less than one second. As the contactor opens, it draws an arc which acts as a series resistance in the circuit thus expediting the decay of the current through the closing coil and the flux linking it. The transient voltage which accompanies the opening of the coil circuit of an inductive device is due to the rapid transfer to the electric circuit of energy which has been stored in the magnetic circuit. In accordance with Lenz's law, the decaying flux induces a voltage in the coil which tends to keep the current constant and in the same direction. The magnitude of the induced voltage is given by the fundamental relation

$$e = -L\,di/dt \qquad (3)$$

The solenoid, which has been receiving energy from the battery, becomes a source as soon as the current starts to decrease, and since the direction of current does not change, the potential polarity of the solenoid reverses. Normally the circuit through the ground lamps holds the battery terminals at a fixed potential with respect to ground; the negative terminal and the end of the coil connected thereto having a potential of negative 60 volts. Starting at the normally negative terminal of the coil and proceeding through it, the induced voltage causes the potential to become more negative until the other terminal, the contactor terminal, and the intervening wire will be negative with re-

Figure 3. Schematic wiring diagram of a circuit breaker being operated, another similar circuit breaker, and instantaneous relay circuit of a bus protective scheme

G indicates initial breakdown to ground at point of maximum potential causing a large potential difference between circuit elements shown in heavy line and ground. It may result in additional or secondary breakdowns at other points such as 1, 1A, 2, 2A, or 3



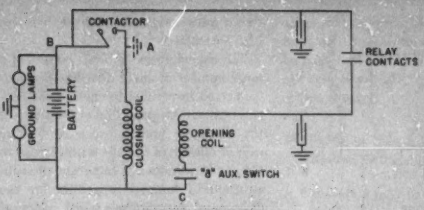


Figure 4 (above). Diagram of circuit involved in transient voltage build-up across relay contacts

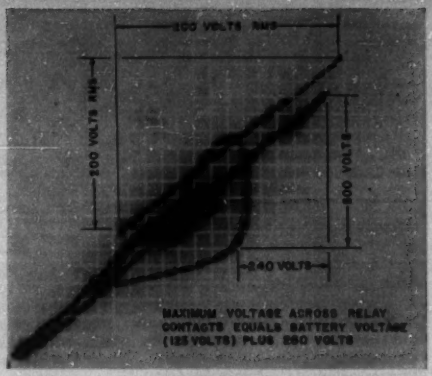


Figure 6 (right). Oscillogram of transient voltage build-up across relay contacts with capacitance to ground of opening wire exaggerated

spect to ground by an amount equal to the sum of the induced voltage and one-half of the battery voltage. Since the sum of all of the voltages around the circuit must be zero, there is a voltage of opposite polarity equal to the sum of the induced and battery potentials across the opening contacts of the control contactor. This condition is shown in Figure 2, which is a schematic wiring diagram of the control of the circuit breaker which is operating, one similar circuit breaker position, and the current transformer and instantaneous over-current relays of a bus protective scheme

Figure 3 shows the same control wiring but with a ground at the contactor terminal of the closing coil, either existing previously or occurring due to the discharge voltage of the coil. This is the only point subjected to extreme potential to ground until breakdown occurs. Unless other unintentional grounds exist, the entire station control now becomes positive with respect to ground by an amount approximately equal to the discharge voltage. Usually the first noticeable effect of such a ground is the violent destruction of the ground lamps. Their filaments are burned out and their glass envelopes often are broken and occasionally blown from their bases. In Figure 3 several parts of the control wiring are shown at ground potential in turn due to grounds which may already exist but more probably are caused by insulation breakdown resulting from the surge voltage. Assume a ground at point 1 which may be any place between the closing switch and the coil of the closing contactor. The contactor will be energized and will reconnect the closing coil to the battery thus suppressing the surge voltage immediately. Should both grounds persist, the contactor will remain closed and the closing coil will be destroyed unless successfully protected by fuses, which is difficult, however, should the restoration of near normal potential by the closing of the contactor free one or both of the grounds, the contactor will drop out, initiating another discharge surge, again energizing the contactor, thus causing a rapid pumping of the solenoid. Should this ground occur at point 1A in the closing circuit of another position, it would energize the closing coil of a closed circuit breaker or close an open circuit breaker. This latter could result in serious injury and equipment damage, for example, if the circuit breaker put a large standing machine across the line especially if it were done while an operator was opening or closing disconnect switches. A fault of this type

Figure 5. Oscillogram of transient voltage build-up across relay contacts with normal circuit constants

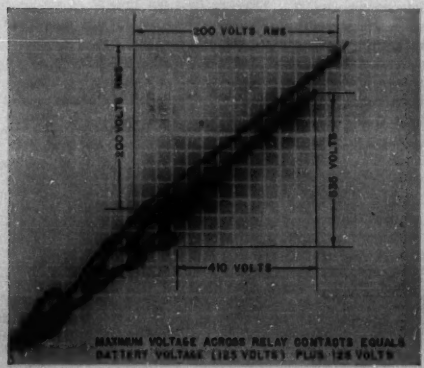
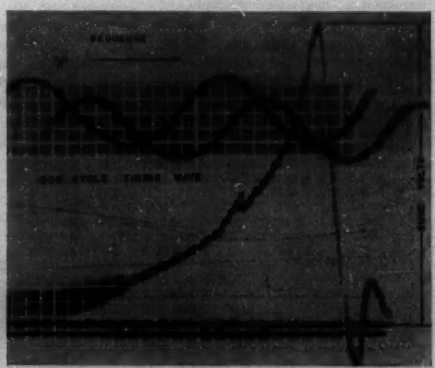


Figure 7. Oscillogram of discharge transient of 125-volt relay



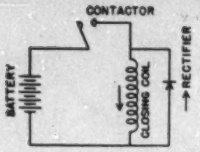


Figure 8. Rectifier used to suppress discharge voltage

was experienced while starting a 400-horsepower 250-volt d-c motor which drives a water circulating pump. The normal starting sequence is to close the positive circuit breaker, decrease resistance across the negative circuit breaker in steps to bring the motor up to speed, and finally close the negative circuit breaker. The discharge voltage of the closing coil of the positive circuit breaker caused a ground on the closing wire of the negative circuit breaker thus energizing the closing contactor and closing the negative circuit breaker. The motor, being thrown directly across the line, received a severely burned commutator and badly dislocated commutating field coils, one of which was forced against a shunt field connection with sufficient violence to sever the connection and open the shunt field.

A ground at point 2 would energize the opening coil of the closing circuit breaker resulting in an apparent trip-free operation. A similar ground on another position, point 2A, would open a closed circuit breaker and could interrupt service. Either of these grounds would destroy the pilot light which monitors the opening circuit and indicates the closed position of the circuit breaker. Another type of fault caused by solenoid discharge voltage shows a breakdown of insulation at point 3 between the contact and coil circuit of a protective relay. In the case illustrated, the relay is in a bus protective scheme and the return path is through the relays and current transformer to the current-transformer ground. This particular fault occurred in a major switching station,

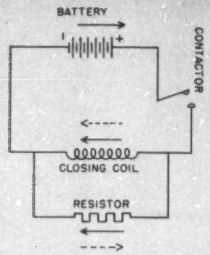


Figure 10. Resistor used to suppress discharge voltage

opening nearly all of the lines on a large bus, and interrupting service to a number of large customers.

Some investigators have reported that the electrostatic capacitance of control circuits materially influences the discharge voltage of inductive devices.[1] The capacitance-to-ground of the d-c control circuits of some typical switching stations was measured and found to vary from 0.05 microfarad for a small indoor substation, using rubber-covered wire in steel conduit for control, to 6.0 microfarads for a large outdoor station having many long runs of lead-covered control cable. Taking these values as representative, a study was made to determine their effect on the discharge voltage of both solenoids and control relays. By referring to Figure 1, it is seen that the measured capacity is divided into two parts, one between the positive bus and ground, and the other between the negative and ground. Considering the closing solenoid with no unintentional ground on the control circuit, the only effective capacitance subjected to high voltage is that of the positive part of the closing coil itself, one terminal of the contactor and the intervening wire, a value too small to produce a measureable effect. A ground on the positive terminal of the closing coil, however, ties this point to the common of the two capacitances, whose other terminals are connected together through the battery, and the induced voltage of the coil charges them to a high potential.

The capacitances involved consist of a large number of small distributed values connected in parallel to each of the control busses. An inspection of the shape of the discharge voltage wave shows the rate of change of voltage with respect to time to be low enough to permit the substitution of lumped capacitors for the distributed capacitance. The effect of capacity on discharge voltage has been analyzed and a mathematical treatment of it presented elsewhere.[2] Analysis of a large number of tests, made with different closing solenoids and with capacitors varying from zero to 6 microfarads, showed that this amount of capacity had no material effect on the peak voltage of the discharge, although it did alter the wave form by making the tail steeper and oscillatory.

The results obtained with relays were less uniform. In general the presence of capacitance paralleling either the relay coil or the de-energizing contacts decreases the discharge voltage. There appears to be a critical value, which for most 125-volt control relays is quite low, that increases the discharge voltage. One relay having a 2,000-ohm coil with a normal discharge voltage of 5 kv when de-energized by a snap switch, while less consistant, gave values as high as 7 kv when supplied through a short length of coaxial cable which had a capacitance of approximately 200 micromicrofarads. Should a breakdown to ground occur so that the capacitance of the station control as well as the ground detector lamp parallels the relay coil, the discharge voltage is effectively reduced. Due to the relatively small amount of energy involved, the discharge voltage of control relays and even opening coils of circuit breaker operating solenoids does not present as serious a problem as does the discharge voltage of closing coils.

Many of the high-speed protective re-

Figure 9. Oscillogram of discharge transient of closing coil suppressed by rectifier

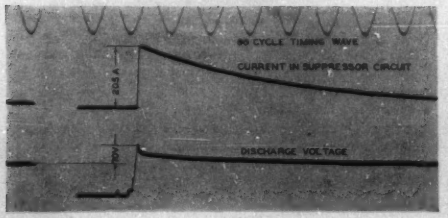
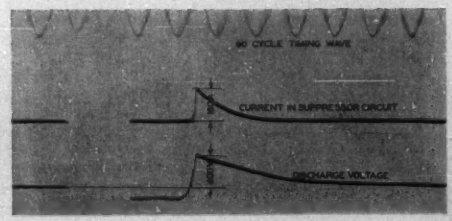


Figure 11. Oscillogram of discharge transient of closing coil suppressed by silicon carbide disk



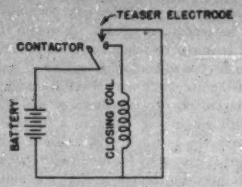


Figure 12. Teaser electrode used to suppress discharge voltage

lays must have their contacts adjusted to a very small gap and there is considerable interest and some apprehension as to the magnitude and effect of the potential which appears across the open contacts during the discharge of an inductive device. Tests which have been made by manufacturers of protective relays show that with a contact spacing of 0.010 inch the 60-cycle rms breakdown voltage will exceed 1,000 volts. Figure 4 shows the conventional method of connecting a circuit breaker operating solenoid. With only the battery lamps grounded, the interruption of closing coil current will not alter the potential at either terminal of the opening coil circuit and the voltage across the protective relay contacts will remain equal to control battery voltage. With a ground at point A so that the surge voltage raises the entire battery and control wiring to a high potential above ground, the potentials at points B and C again differ by the battery voltage. Since the inductance and capacitance of the wire between B and the positive contact of the relay is low, the capacitance will charge rapidly without oscillation and the potential at the positive contact will be very near the same as at B at all times. In the opening coil side of the circuit the small capacitance of the opening wire must charge through the high inductance of the opening coil. With a suddenly applied voltage, the circuit oscillates and a voltage equal to the battery voltage plus the peak voltage of the oscillatory wave will exist across the open contacts. Figures 5 and 6 are oscillograms showing this voltage to be moderate and with contact spacings of not less than 0.010 inch no breakdowns are expected.

Methods of Surge Voltage Suppression

Equation 3 may be rewritten:

$$e = kN^2 di/dt \qquad (4)$$

The factors which make up k, that is the size, shape, and material of the magnetic circuit are determined within reasonable limits by the characteristics of the load including total work, force-travel relations, and frequency of operation, and to a less extent by cost and efficiency. These same factors conjointly with the source voltage also determine the size of the coil wire and the number of turns, N. Since both k and N are fixed by design considerations, the factor di/dt affords the most ready means of controlling the discharge voltage. The current in this factor includes currents which are induced in the magnetic material and in contiguous conducting parts as well as the current in the coil itself. Any decrease in di/dt will increase the time interval between the removal of the supply voltage and the drop-out of the device; in other words the flux requires a longer time to decay to the pull-out value. In some relay applications it may be undesirable to retard the opening, however, this is not generally true. In the circuit breaker closing problem, a trip-free opening followed by an immediate reclosure is the only operating sequence, so far conceived, which is likely to be adversely affected by decreasing the rate of current decay. Such a requirement is not considered imminent. As a sudden decrease or interruption of the coil current of a solenoid having a solid iron magnetic circuit causes the transfer of a large proportion of the ampere turns to the iron, it follows that any modification of the core iron to obtain faster closing, which may vary from a single radial plunger slot to complete lamination of the iron, will increase the time rate of current decay and consequently the induced voltage. To summarize, any design intended to increase pick-up speed by expediting the build up of the flux will usually encourage its rapid collapse. Several methods for decreasing the rate of current decay are suggested and most have been proved in service.

Dry-Plate Rectifier

The functioning of a one-half wave rectifier to suppress the discharge voltage of an inductive device is predicated on the reversal of potential polarity at the inception of the discharge. The device is connected in parallel with the coil as shown in Figure 8. It presents a high resistance to the flow of current while the coil is energized and a low resistance to the discharge current which flows through it in the opposite direction. The arrows show the direction of the current during the discharge. All of the types of dry-plate rectifiers work satisfactorily provided they have sufficient plate area and number of junctions. The authors have had considerable experience with this application of rectifiers and believe that the method has several advantages over any other that has come to their attention. Rectifiers are very effective as voltage suppressing media, limiting the discharge voltage to less than operating voltage, do not add appreciable load to the current source, materially decrease contactor duty and maintenance, and have long trouble-free lives. About 500 of these devices were installed on large solenoids before the war and an equal number since war-time restrictions were removed. There have been approximately five failures all due to increase in forward resistance and loss of effectiveness. All of these failures were discovered by operating personnel who observed the larger and more intense arc and the sharper noise when the contactors opened. The rectifier must be connected to the circuit with correct polarity and a fuse to isolate it in case of failure is recommended.

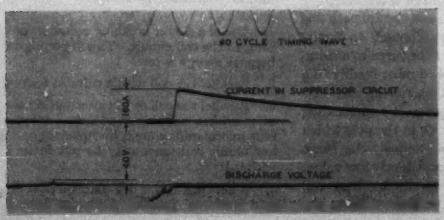


Figure 13. Oscillogram of discharge transient of closing coil suppressed by teaser electrode

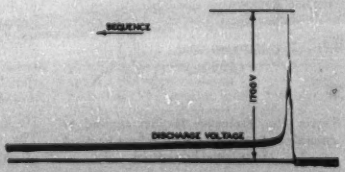


Figure 14. Oscillogram of discharge transient of closing coil unsuppressed

Parallel Resistor

One of the oldest and best known means of discharge voltage suppression consists of paralleling the coil with a suitable resistor as shown in Figure 10. The solid arrows show current direction in the different branches with contactor closed and the broken arrows after the contactor has opened. Assuming arcless opening of the contactor and letting r_1 be the ohmic resistance of the coil, r_2 the resistance of the paralleling resistor, E_B the voltage of the control battery, and E_C the initial voltage across the coil, the steady state coil current is

$$I_1 = E_B/r_1$$

With all of this current transferred to the resistor at the instant the battery circuit is broken, the voltage across the coil and resistor in parallel is

$$E_c = I_1 r_2 \text{ or}$$

$$= E_B r_2/r_1 \qquad (5)$$

From this relation, it is evident that to limit the discharge voltage to a satisfactory value, the resistor must be low enough to draw an appreciable current while the coil is energized. When used with solenoids which are operated from a control battery, the additional current increases the battery duty and the voltage drop in both the battery and the connecting wires. With larger circuit breakers operating at higher speeds and with higher system short circuit currents, the maintaining of satisfactory voltage regulation at the solenoid is becoming increasingly difficult and care should be exercised in applying any device adversely affecting it. The method has proved entirely satisfactory when used with 250-volt closing coils which are supplied from a high-capacity source. It cannot be used with coils which require continuous monitoring.

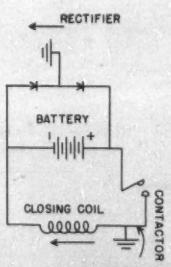


Figure 15. Protective rectifier applied to control bus to limit discharge voltage transient after initial breakdown

Silicon Carbide Resistor

This method consists of paralleling the closing coil with a resistor whose resistance element is made of silicon carbide which has the well-known lightning arrester characteristic. Suppose a resistor having the characteristic

$$e = 100 \cdot i^{0.38}$$

is used to limit the discharge voltage of a coil having 0.5-ohms resistance and energized at 115 volts. The current drawn by the resistor at 115 volts is

$$= \left(\frac{E}{100}\right)^{\frac{1}{0.38}}$$

$$= 1.65 \text{ amperes}$$

The current through the closing coil at 115 volts is 115/0.5 = 230 amperes. Assuming that the contactor gives arcless performance and that the entire current is transferred to the discharge resistor, the discharge voltage will be

$$e = 100 \cdot 230^{0.38}$$

$$= 460 \text{ volts}$$

This example demonstrates the superiority of a material having a nonlinear characteristic over the conventional resistor. It does not consume sufficient current during the closing period to materially affect the voltage regulation at the solenoid and it does hold the discharge voltage within reasonable limits. Its use does not preclude continuous monitoring by conventional schemes, however, some modification may be necessary. Its characteristic is nondirectional which somewhat simplifies the installation. Units for this service are available and are furnished with some of the larger circuit breaker closing solenoids.

Teaser Contact

The so-called "teaser contact" consists of an electrode so located that it will be in the arc path as soon as the contacts separate and connected to the other side of the closing coil as shown in Figure 12. This method has given satisfactory service for several years with a few types of contactors. The location of the teaser electrode is critical as it must be in the arc path and near the stationary contact to ignite promptly and to hold the surge to a suitable value. It is applied most readily to a contactor having two series breaks. By removing the blowout coil and locating the additional electrode in the pole which is connected to the solenoid, the surge voltage is held to a consistently low value. The other break with its blowout coil quickly interrupts the current from the battery and precludes any possibility of a battery short circuit. A special study is required to tailor the arrangement to each type of contactor and rapid erosion of the arc chute may result from the impingement of the discharge arc. Too, with single-pole contactors there is always the possibility of a battery short circuit caused by failure to extinguish the arc between the teaser electrode and the moving contact. A fuse, selective with the main control fuse, is recommended so that in the event of a battery short circuit, the main fuse will not blow, leaving a closed circuit breaker without available tripping power.

Methods Applicable to Relays

The energy which can be stored in the magnetic fields of relays and its potentiality of damaging equipment or injuring personnel is very small when compared with that of circuit breaker closing solenoids and other large magnets. The very high discharge voltage of some relays is explained by equation 3 which shows the voltage to be proportional to the square of the number of turns, and by the fact that many relays have laminated iron structures. Despite the high voltages encountered, the authors have experienced a surprisingly small amount of trouble which could be traced directly to this source. It is very probable that the high voltage has damaged insulation but that the energy has been insufficient to effect a complete breakdown. Such damaged insulation may be broken down by subsequent periodic high-voltage tests. In general the same methods which are effective for the larger devices can be successfully applied with relays. With specific reference to the first method suggested for relays and which works very well, tests made on large devices with a contactor which had been slowed up mechanically and had its blowout coil removed showed only a moderate discharge voltage. However, the long duration of the arc increased the erosion of both contacts and arc chute sufficiently to render the method impractical where large energies are enountered. The following are particularly suitable for relays:

1. Use of a slower breaking switch or relay to open the coil circuit. This is permissible because energy is low.
2. Parallel the coil with a suitable resistor.
3. Parallel the coil or the interrupting contacts with a suitable condenser. (One to four microfarads should be sufficient and a low ohmic resistance may be put in series with it.)
4. Where justified, a dry-plate rectifier or a cold cathode gaseous discharge rectifier

similar to the type *OZ-4* affords excellent surge protection for small relays.

Protective Rectifier Applied to a Control Bus

There is one other method that, while it is not used to suppress discharge voltages directly, is so closely allied to the whole problem that it should be described here. It consists of a half-wave or blocking rectifier connected across the battery with its mid-point grounded as shown in Figure 15. Each half of the rectifier selected for this application should be capable of withstanding full battery potential in the blocking direction and of discharging the largest solenoid in the station several times in rapid succession. It should have a high reverse resistance as any current drawn by the rectifier raises its temperature and of secondary importance increases the battery load. Its resistance should be stable because unbalance between the two halves affects the functioning of the ground detector lamps.

A rectifier so applied, does not normally limit or control the discharge voltage nor does it decrease the contactor duty as do most other methods. It does provide at all times a low-resistance high-capacity path to limit the voltage between the battery and ground, should a failure to ground occur due to discharge of any inductive device connected to the battery. In the event of a failure to ground, as indicated in Figure 15, the discharge current flows from the coil, through the rectifier to ground, and returns to the coil through the insulation failure; thus affording a low-resistance discharge circuit and holding the battery-to-ground voltage to a safe value. Most of the danger to personnel, service, and equipment which arises from discharge voltages is caused by imposing a high voltage, backed up by considerable energy, between the station control and ground. By reducing this danger to a minimum, a rectifier connected from battery to ground accomplishes most of the important functions of any of the other methods which must be applied to each inductive device. Its chief virtue is that but one per station is required and that it can be put in operation with a minimum of time, labor, and material. Its limitation is that high voltage and possible breakdown at the danger point between the coil in question and the de-energizing contacts are not prevented, but their effects are minimized.

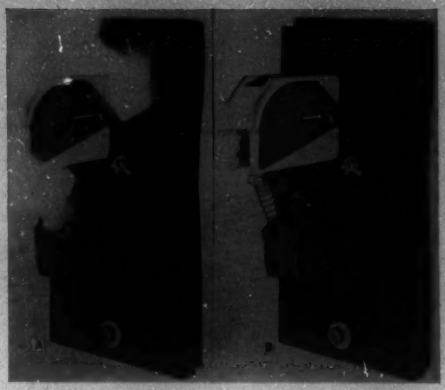


Figure 16. Typical control contactor interrupting closing coil current of 250 amperes

A — Transient volage not suppressed
B — Transient voltage suppressed by discharge rectifier

Conclusions

1. There is a high voltage produced by the decay of flux when an inductive device is de-energized. Uncontrolled, it may injure personnel, interrupt service, and damage equipment.

2. The magnitude of this voltage depends on the value of the energizing voltage, the size, shape, and material of the magnetic circuit, the number of coil turns, and the speed of de-energizing. The discharge voltage of a small relay is often higher than that of a large circuit breaker closing solenoid.

3. The energy stored in the inductive circuit varies approximately with the size of the device.

4. A dry-plate rectifier paralleling the coil of each inductive device holds the discharge voltage to a safe value. A slow acting switch, a small capacitor (1 to 4 microfarads), a small resistor, or a gaseous discharge rectifier performs well when used with small low-energy devices such as control relays.

5. A dry-plate rectifier connected across the control battery with its mid-point grounded prevents the communication of high discharge voltage between circuits.

6. All these methods of reducing the surge voltage involve slowing the transfer of stored magnetic energy to the electric circuit, which in turn delays the dropout of relays or solenoids. This delay is usually not objectionable but must be evaluated carefully.

References

1. Voltage Surges Caused by Contactor Coils, J. R. Taylor, C. E. Randall. *Journal*, Institution of Electrical Engineers (London, England) volume 90, part 2, April 1943, pages 90–5, discussion 95–100.

2. Switching Surges with Transformer Load-Ratio Control Contactors, L. F. Blume, L. V. Bewley. Electrical Engineering (AIEE Transactions), volume 56, December 1937, pages 1464–75.

Discussion

J. H. Kinghorn (American Gas and Electric Service Corporation, New York, N. Y.): Halman and Harris have presented a clear picture of the conditions which can produce high transient voltage on control circuits and the conventional methods of limiting this voltage. They indicate that while such voltages can be produced by de-energizing any type of inductive device, it is circuit breaker closing solenoids which are the principal causes of subsequent damage to other parts of the control system because of the greater amount of stored energy involved. This agrees closely with experience on the system with which the writer is associated. In the few cases of insulation failure which could be attributed to high induced relay coil voltage the damage was confined to the insulation of the coil itself, and did not produce further breakdown elsewhere in control circuit.

The methods of voltage suppression described all depend for their effectiveness on providing a discharge path for the coil current. This means additional equipment in the form of discharge contacts, resistors, or rectifiers, all of which are subject to failure which can easily go undetected. It would appear that a more direct approach might be to limit the possible induced voltage by the design of the device or to provide sufficient insulation strength in the coil and associated contactor connection to prevent breakdown. The latter may be an unreason-

able approach, particularly since this insulation is sometimes subject to exposure to moisture. The former, however, implies some means of delaying flux collapse by suitable design. The authors point out that magnetic circuit design which will delay flux collapse also usually delays flux buildup and consequently increases to some extent the pickup time. It would be helpful if some figures could be presented showing the relative magnitude of these effects. Perhaps sufficient modification of design could be tolerated to limit induced voltages to a value which would require only a moderate increase in insulation strength to assure trouble-free operation. This same line of reasoning can be applied to relays, except that in this case there are some applications where no delay in pickup time is acceptable and any method of delaying flux decay must not increase it. Again, however, an increase in relay-coil insulation strength combined with magnetic circuit revision may offer a practical solution.

J. Russell Taylor (Igranic Electric Company, Ltd., Bedford, England): As co-author of the first paper mentioned in the references, I am most interested in the investigations of Halman and Harris.

Randall and I studied the surges caused by contactor coils, and our tests showed that the general tendency was towards an increase of surge voltage with increase of contactor size. For any given coil the maximum voltage surge always corresponds with an optimum capacity across the coil.

The present authors state that the amount of capacity across the coil has no effect on the maximum surge voltage, but merely on the shape of the tail. Our investigation with contactor coils on 230-volt direct current having resistances varying between 2,000 ohms and 400 ohms fitted to contactors of 150 amperes to 1,200 amperes, showed conclusively that there is a sharply defined optimum capacity for each size of coil which gives the highest surge voltage. With capacities smaller than the optimum there is a fairly rapid drop in the surge voltage, but with capacities larger than the optimum, the lowering of the surge voltage is much more gradual.

Possibly the discrepancy lies in the greater power dissipation of the circuit breaker closing coils mentioned in the present paper than with continuously rated contactor coils. It may be that the former coils with their heavy current alter the effect of the arc at the switch contacts.

In view of the very large voltages produced across the contactor or solenoid coil it is indeed surprising that coil and insulation failures are not much more common than they are.

Have the authors evolved any means of predetermining the surge voltage from the dimensions of the coil and the magnetic circuit?

V. J. Hayes (Connecticut Light and Power Company, Waterbury, Conn.): This interesting paper by Halman and Harris is of importance not only to relay engineers but to those who are concerned with maintenance in stations that employ d-c control systems. The statement in this paper that the discharge of small relay coils can produce surge voltages from two to three times those of the larger solenoids should be of particular interest; because some stations (such as generating plants) may use a considerable number of such relays in control of auxiliary equipment, and these devices are usually subject to more frequent operation than are large circuit breakers. Since it may not appear practical or economically justifiable in such cases to add shunt resistors or dry-plate rectifiers to each of these relay coil circuits, the use of the one protective rectifier applied to a control bus becomes attractive, as described under the heading Protective Rectifier Applied to a Control Bus in the paper.

The use of the shunt resistor (suitably protected by a fuse) across circuit breaker trip coils is of interest not only for its d-c surge voltage limiting feature, but because the current drawn by this resistor when the trip coil is energized reaches its full value almost instantly. This characteristic is often helpful in obtaining faster and more positive operation of seal-in contactors and devices in tripping relays, which otherwise would normally have to depend upon the relatively slower buildup time of the current in the trip coil proper.

E. L. Michelson (Commonwealth Edison Company, Chicago, Ill.): This paper by Halman and Harris is an excellent contribution to the literature on voltage surges in control circuits. Their paper gives a very clear picture of the initiation of these surges, the manner in which they may cause troubles of various kinds, and the possible methods of designing the circuits in such a way as to minimize the effects of transient voltages.

The AIEE relay committee recently set up a project committee to study the insulation of relay control circuits. This paper was initiated through the efforts of the project committee, and will be a valuable part of the work of this project committee. The project committee has been attempting to work out a reasonable co-ordination between the maximum permissible voltage in the control circuits and the design of relays, circuit breaker controls, and other items that make up the control circuits. At the present time, it appears that satisfactory operation will be obtained if the maximum transient voltage in the control circuits is limited to 1,500 volts crest value. This will result in co-ordination between the circuit breaker control wiring standards and the permissible relay test voltages. The Association of Edison Illuminating Companies (AEIC) specifications for oil circuit breakers specify that the inductive kick caused by interrupting closing currents shall be less than 1,500 volts crest. The AIEE relay standard, which is now being reviewed, will recommend a test voltage for relay circuits of 1,125 volts rms.

There have been a few instances where voltage surges in control circuits have been caused by other phenonena besides the inductive kick of relay and circuit breaker coils. One case with which I am familiar was caused by the capacitive coupling between the 66-kv cables and the control wiring of a relay circuit. Sudden application of voltage to the high-voltage cables caused sufficient transient voltage to flash over the contacts of a protective relay and cause a transmission line to be opened. In this case the condition was corrected by the installation of capacitors in the control circuit.

J. A. Elzi (Commonwealth and Southern Corporation, Jackson, Mich.): The phenomenon so well described and analyzed in this paper is one which many of us may have encountered in the past but which has not in general been given due recognition. The AEIC specifications for alternating current power circuit breakers takes cognizance of the voltage induced by circuit breaker solenoids and specifies that: "The transient voltage due to the interruption of the control current shall be limited to 1,500 volts crest." This we understand is about the practical limit and it is noted that the authors have measured transient voltages of 1,700 volts for 125-volt solenoids and 2,500 volts for 250-volt solenoids.

From the work now being engaged in by the relay committee it appears that it may be found difficult to provide relays having a withstand test voltage in excess of 1,500 volts rms 60 cycles for one minute when new. The recommended test voltage for field tests will probably be somewhat below this.

These facts strongly indicate the necessity of co-ordination between the dielectric strength of control circuits and devices and the transient voltage to which they are subjected. Considering the practical limitations involved, it seems most logical to provide means in the control circuits or devices such as the authors have described to reduce the transient voltages.

Several cases of incorrect relay operation due to high induced voltage are mentioned in this paper. We strongly suspect that others of us may have had similar incorrect operations without recognizing their true cause.

This phenomenon is not confined to control circuits. We recall an instance in which trouble was experienced due to flashover of the rheostat shunting contactors of a voltage regulator in a quick response excitation system. In this case there were two contactors each short circuiting a block of resistance in the main exciter field circuit. The transient voltage across each contactor when it opened was found to be 2,100 volts. In these larger contactors it was practicable to provide adequate insulation to withstand this voltage, but repeated stresses of this magnitude may be conducive to ultimate failure and a means of reducing them is highly desirable.

Tony Offenbacker (Indianapolis Power and Light Company, Indianapolis, Ind.): The investigation of the false relaying that occurred on pilot wire relays that have contact separation of 0.01 inch or less indicated definitely that overvoltage surges or transients were causing the contact breakdown. These conditions took place on a control system that had no unintentional circuit grounds. The discussion in the paper seemed to indicate that no overvoltages occurred on this type circuit arrangement.

The type of measuring equipment has a great bearing on the results obtained. Using the magnetic oscillograph, no overvoltages or transients could be detected, but by using the trigger action of a gas tube, many operations resulted. The cathode ray oscilloscope will show quite a lot of oscillations

and pulsations when placed across a battery during various current carrying circuit interruptions. Probably a very sensitive transient recorder would show more.

When experimenting with interrupting current carrying circuits using the trigger action of the gas tube as a measuring device, it was very noticeable that operation of the gas tube did not depend so much on the type circuit interrupted as it was on how much an arc the interruption caused. The circuit interruption of an ordinary light, if done slowly to cause arcing, would set off the gas tube. Of course, any inductive circuit would tend to arc with greater intensity to create more disturbance.

If an arc sets up a disturbance composed of many frequencies because of the distributed capacity of the inductive coils and the inductance of these coils, some one frequency may be of such value so as to set up an oscillatory condition. This condition gives rise to a localized high voltage either high or low frequency. A potential of high enough value may be obtained to cause breakdown of the relay contacts. The follow-up power flow would then complete the equipment operation.

If the theory that high frequencies are set up in the various circuits due to arcing is true, ways and means of measuring and controlling them must be found. The methods of surge voltage control suggested in the paper may not be satisfactory. Even the use of a capacitor across the battery should be made with caution to prevent making the condition worse.

Since most relay contacts have a separation in excess of 0.01 inch, not much trouble is to be expected with the present type relays. On the other hand many relay schemes are being advanced using electronic equipment with their small contactors and close contact separation so the problem may become more serious in the future.

Lacking the necessary sensitive measuring and recording instruments, the foregoing theory cannot be definitely proved, but it seems there is some other condition taking place than the inductive surge.

T. R. Halman and L. K. Harris: The discussion on this paper is greatly appreciated and leads us to believe that the subject is of general interest and that the experiences, troubles, and even the thinking of other operators closely parallel our own. The incident depicted by Michelson shows that not all high-voltage surges on control circuits originate with devices electrically connected to them. It is surprising that there is sufficient electrostatic coupling between the 66-kv cables and the control wiring to induce a voltage capable of breaking down relay contacts, even when special grounding methods are used to reduce sheath losses.

We have had little experience with high speed excitation and have not encountered trouble similar to that described by Elzi. It appears to be a case where increasing insulation and dielectric strength is the only feasible solution. We have considered the use of suppressors to protect synchronous capacitor field windings when the field circuit breaker is suddenly opened. No applications have been made.

We were very pleased to receive Taylor's comments since he and Randall had made a thorough study and published a comprehensive paper on a different phase of the same subject. While the premise of an optimum capacity for each device was not worked out completely in our paper, it was accepted and some readings were obtained to verify it. Our statement that the amount of capacity across the coil has no material effect on the maximum surge voltage was limited to 125-volt closing solenoids having coil resistances between one-half and $1^1/_4$ ohms and connected to control busses involving total capacitances ranging from 0.05 to 6.0 microfarads. These capacitances were much below the optimum values for the respective coils. We have not evolved a method of determining the magnitude of the surge voltage from physical dimensions of the coil and magnetic circuit. Such a relation, if worked out, would include the speed of current interruption, that is, the type of control contactor. Modern solenoids, using cast steel or fabricated steel plate for their magnetic circuits, show appreciably lower discharge voltages than earlier ones which used cast iron. The decrease can be very largely attributed to the lower resistivity of the steel.

The use of a resistor, suitably fused, in parallel with the trip coil of a circuit breaker, as suggested by Hayes, is one of those rare applications where one piece of equipment gives two independent beneficial effects. We have experienced some minor difficulties with this seal-in problem when using lockout relays in series with trip coils. They have been overcome by selecting relays with low pick up values and with shorter time constants than the associated trip coils. We have not used the paralleling resistor because it would impair the significance of our standard trip coil circuit monitoring.

Kinghorn's experience with relays supports our contention that while very high surge voltages may be produced in relay coils, should an insulation breakdown occur, the surge is effectively suppressed by the battery grounding lamps and the capacitance to ground of the control wiring. Regarding reliability and detection of failures, our experience, covering about ten years indicates that a rectifier possesses a high degree of reliability in this service. Also with periodic inspection and maintenance there is small probability that a failure will be long undetected.

We believe that the AEIC specifications, which limit the transient discharge voltage to less than 1,500 volts crest, have established a value about as low as it is feasible to meet by practical design. This value will be altered by the type of control contactor used (speed of current interruption); a factor often not under the control of the designer. Increasing coil insulation to withstand higher voltage would logically result in larger and more expensive solenoid mechanisms. It could not be applied to existing solenoids and they will continue to be serviceable for many years. As Michelson points out, the AIEE relay standard will recommend a test voltage of 1,125 volts rms for relay circuits. This corresponds to about 1,600-volts crest. It will be applied at relatively long intervals, from a low-power source, to equipment which has been removed from service, and at a time when facilities are available to repair possible damage. We believe that such a test voltage should be much higher than a voltage caused by the normal functioning of equipment and which has sufficient energy to do serious damage.

Offenbacker's discussion outlines an interesting problem of high frequency oscillations on a tripping bus which cannot be summarily dismissed. He offers an ingenious method of determining the peak value of high-frequency low-energy surges and the capacitor which he uses affords a practical means of suppression. We are somewhat at a loss to know why we have not encountered similar troubles. There are two plausible explanations which occur to us. We are using high-speed relays with close contact spacing only in large stations where the capacitance to ground exceeds 1 microfarad. This capacitance may be effective in suppressing the high-frequency surges. Also we connect each trip circuit directly across the main control bus instead of using a tripping bus.

If the tripping bus has very low capacitance, as seems probable, a low-energy transient may cause a high-frequency high-voltage oscillation. We were not able to trip a gas-tube relay set a few volts above battery voltage even by interrupting the field of a 50-kva alternator or the coil of a circuit breaker closing solenoid. Similar operations showed a moderate-frequency transient with a crest value not exceeding 15 volts when viewed on a 5-inch oscilloscope having an input impedance of 2 megohms.

Electronic Protective Relays

R. H. MACPHERSON
ASSOCIATE AIEE

A. R. van C. WARRINGTON
MEMBER AIEE

A. J. McCONNELL
ASSOCIATE AIEE

IN THEIR PRESENT state of development, electromagnetic protective relays possess characteristics and reliability well abreast of the demands of power system protection. However, the attainment of this status has been fraught with many problems for which ideal solutions have not been found. Further development in system design, such as high-speed reclosing, together with the increasing demand for higher degrees of accuracy, speed, and dependability, have accentuated these problems to a point where the inherent limitations of electromagnetic relays are raising barriers to the achievement of desired goals.

The increasing use of electronic circuits in nearly all branches of electrical engineering, coupled with the great strides that have been made recently in the design and manufacture of electronic circuit components, seem to point the way to the use of electronic means for lifting these barriers.

In view of the widespread interest in electronic devices of all kinds, it is not surprising that protection engineers have developed a keen interest in electronic protective relays. This interest, however, has not resulted in general enthusiasm for such relays because many relay engineers are thoroughly satisfied with electromagnetic relays. On the other hand, some operators would welcome a complete line of electronic relays. Some of the latter group have been inclined to attribute the lack of electronic relays to, what is to them, a reactionary attitude. This paper is intended to present both a report of progress, and an explanation of the present limited availability of such relays.

The limited number of electronic relays should not be assumed to indicate any serious lack of interest or ingenuity. To the relay engineer, electronic circuits are merely one of the many tools available for obtaining the characteristics necessary for discrimination between normal and abnormal conditions in a power system. Typical of such tools that have been available to relay engineers for many years are nonlinear resistors, rectifiers, and neon lamps.

Early Work

The following examples show that electronic relays are by no means new, and that manufacturers have been aware of their potentialities ever since practical tubes were available.

In 1927, A. S. Fitzgerald developed an electronic pilot relay system[1] (Figure 1) to overcome the limitations of pilot wires when operating over considerable distances. This scheme was abandoned because of the short life and the high cost of tubes available at that time, but a somewhat similar scheme is now in active use as the phase-comparison relay[2] (Figure 2). Fitzgerald's was the first well-known application of electronic protective relays.

In 1931, Rolf Wideröe of Norway developed electronic circuits for most of the common types of protective relays and described them in a paper presented before the Institute in 1934.[3]

In 1932, in the United States, a laboratory sample electronic reactance relay[4] was built and tested, but at that time the electromagnetic reactance relay was fully adequate for the needs of the industry and no steps were taken to put the electronic relay into production. This type of electronic relay was built because it embodied all the common relay principles in a single relay and hence presented all the problems of the different types, such as over-current, undervoltage, directional, phase angle, and differential relays. In other words, the reactance relay was a good guinea pig. The experience gained from this development was destined to become valuable for future developments.

Directional-comparison carrier-pilot relaying, using electromagnetic directional relays to discriminate (by their joint action) between internal and external faults[5,6] (Figure 3), came out in 1930. This type was extended in 1936[7,8] to include the back-up function in the form of distance relays (Figure 4). Neither of these can be classed as an electronic relay, because the discriminating relays are electromagnetic and the electronic circuit is limited to transmitting and receiving signals along the transmission line; i.e., only the communicating channel is electronic. On the other hand, this application has been so successful and widespread that it is valuable as a source of information on the life and behavior of electronic components such as tubes, tube sockets, and small resistors.

An electronic synchronizing relay (Figure 5) was built in 1934 and has been used widely ever since. This was a case where the use of an electronic device was justified because it could achieve results not economically obtainable with an electromagnetic relay.

In 1936, an electronic timing meter (Figure 6) was designed for testing high-speed relays. It measured down to one millisecond, and hence was very much better than existing synchronous timers, and was considerably more convenient to use than an oscillograph.

Since then there have been a number of miscellaneous electronic applications

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R. H. MACPHERSON and A. R. VAN C. WARRINGTON are with the relay engineering, switchgear division, General Electric Company, Philadelphia, Pa.; and A. J. McCONNELL is with the central station engineering divisions of the General Electric Company, Schenectady, N. Y.

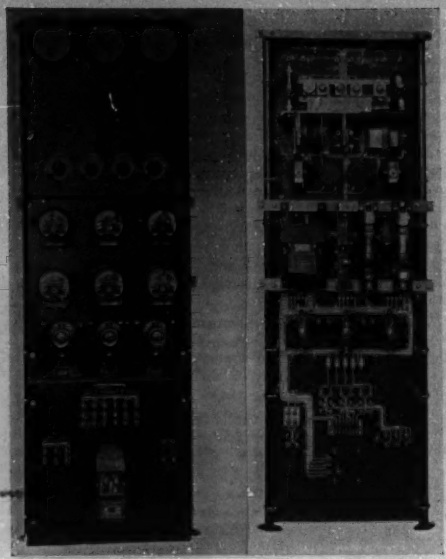

Figure 1. Early electronic pilot relay for carrier-current application

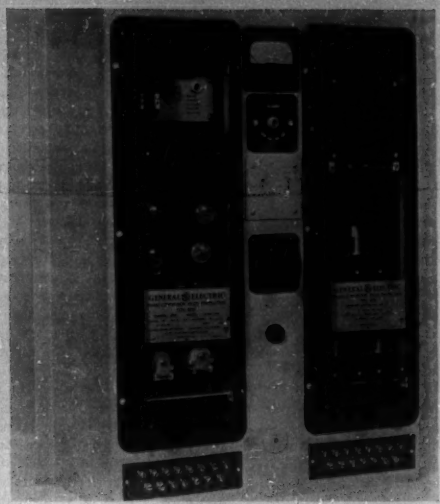

Figure 2. Phase comparison pilot relay

to relays, mostly in connection with the war effort; but, in general during the last ten years, there have been few cases where it was felt that an electronic relay would do the job better than an electromagnetic relay.

Carrier Relaying

Carrier relaying is the principal source of experience with tubes. In the early days of carrier relaying, tubes had a very irregular life and required frequent replacement. This led to the general specification that the tripping of a circuit breaker should not depend upon the proper operation of a tube.

Tubes have improved gradually, so that today the average replacement does not exceed once a year. Improvement in tubes has reduced skepticism to the point where tripping via tubes is considered acceptable. This is shown by the fact that available forms of phase- or current-comparison relaying require tube operation for the tripping function.

Why has carrier-current relaying attained such widespread use, whereas other types of electronic relays are few? The major reason is undoubtedly that carrier relaying has accomplished a highly desirable result not economically feasible by any other known method. This popularity is in spite of known limitations of electronic circuits. In other words, the outstanding advantages of carrier relaying outweigh its minor disadvantages.

Requirements for Electronic Relay

If an electronic relay is to compete with an existing electromagnetic device, it must offer advantages, not just novelty. This is particularly evident when the wealth of experience available on electromagnetic relays is considered.

The most desirable improvements over existing relays are in speed, burdens, maintenance, reliability, accuracy, and reduction of the number of contacts. At the present time it is evident that low burdens can be expected with electronic relays and that the operating time can be around one cycle and consistent, being the same at pickup as at many times pickup. This characteristic alone may justify the use of tubes in the protection of transmission lines with ultrahigh-speed reclosing.

The cost of electronic relays depends on their complexity. A simple relay, such as a time overcurrent relay, would be much more expensive than the magnetic version. However, a complex relay such as a synchronizing relay or a distance relay, may be comparable in cost.

Skepticism with respect to electronic relays is based largely on the contention that radio components are not equal to those normally used in relays. Capacitors and resistors present no problem because the standard relay types can be used. This leaves the tubes and tube sockets. The advent of the new long-life tubes may be the answer to the problem of tube life, but this can be found out only by some years of experience. Tube sockets are available with good contact pressure which would appear satisfactory, but here again a period of experience may be necessary.

In any electronic relay circuit, it is essential that the characteristics of the tube should not control the relay characteristics, otherwise the aging of the tube may change the relay characteristics. However, if the characteristic can be restored by a simple adjustment, which can be readily checked by a simple test, then this requirement may be waived. In other words, if a change of a few per cent in pickup occurs in a period of a year, or when a tube is replaced, and it can be restored by a simple adjustment, this condition may be considered satisfactory.

The effect of transients is an important factor because the operation of an electronic relay depends upon the comparison of instantaneous values of current and potential.

The battery drain should be small, if

continuous. Normal variations in battery voltage should not interfere appreciably with the characteristics of the device.

The circuits and their controls should not be so complicated that the ordinary relay test man has to be replaced by a radio engineer.

The Present Outlook

Two things have tended recently to increase the interest in electronic relays: the advent of long-life tubes; and the necessity for consistent low operating times, even at low currents, for more effective use of instantaneous reclosing.

Long-life tubes have been developed by tube manufacturers in a few forms such as a pentode, a high-mu twin triode, and a low-mu twin triode, all of which are useful in relay circuits.

In reclosing after a fault it is essential that both ends of the section trip within a few cycles of the same time. The inertia of electromagnetic relays tends to make the relays at the end remote from the fault operate more slowly than those at the end near the fault, especially during minimum generating conditions. An electronic tube has no inertia, so that it is possible to design the circuit so that relay operating time is not affected by current magnitude or fault location; consequently both ends of the section can be cleared simultaneously.

While these improvements in components and this need for high-speed operation seem to have pointed to the desirability of electronic protective relays, it is not proposed to introduce such relays without first accumulating some practical experience with them.

While electronic relays now can be built to perform nearly any protective function, the question of whether they can do a better job, all things considered, depends upon the application. For instance, the single unit induction cylinder polyphase directional relay is a compact efficient device occupying about the space of a 4-inch cube. The corresponding electronic relay would involve at least a 6-tube circuit of considerable complexity and, with presently available components, would occupy more space and be more costly. Again, the induction time overcurrent relay sets a standard of simplicity and reliability that would be difficult to meet with an electronic circuit. Finally it is doubtful if any electronic means could effectively take the place of any of the more common auxiliary relays.

In short, the electronic relay is a valuable tool which has been long appreciated and which has been used wherever it could do a better job than an electromagnetic relay. As improvements are made in the tubes and their accessories, their use will increase and they will certainly be used for new applications where the existing relays are inadequate.

The Electronic Mho Relay

To evaluate accurately the relative merits of the electromagnetic and electronic relays, it was decided to build a sample 3-zone electronic mho relay and to compare its characteristics with those of an electromagnetic mho relay. The results were sufficiently encouraging to

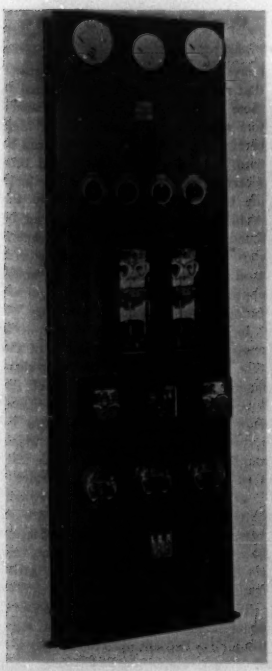

Figure 3. Directional comparison carrier pilot relay

Figure 4. Distance carrier pilot relaying equipment

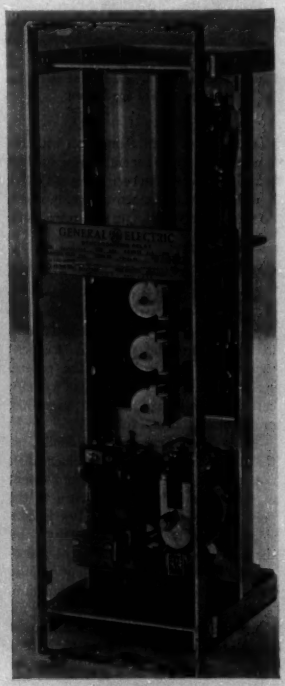

Figure 5. Electronic synchronizing relay

Figure 6. Electronic timing meter

warrant building a three-phase terminal which could be put in service on an actual transmission line for a period of operation which would include a lightning season. The selection of the mho relay was based on reasons previously discussed in connection with an early electronic reactance relay, that is, it combines almost all the principles now used in protective relaying. Successful development of the mho relay would imply that little difficulty would be experienced in building some of the simpler high-speed relays.

Description and Operation

One way to build a relay with a mho characteristic is to compare line voltage with line current at the moment of the line-voltage maximum. A typical unit would consist of three basic elements: a pulsing circuit, a measuring circuit, and a tube circuit. This arrangement is shown in block diagram form in Figure 10.

The function of the pulsing circuit is to generate a pulse at the moment of line-voltage maximum. The pulse is used to overcome a large grid bias and permit a tube to conduct. The function of the measuring circuit is to compare the line voltage with the line current. It consists of a transformer supplied with line-to-line voltage and a transactor (defined later) supplied with the corresponding line currents. The secondary of the transactor is provided with a voltage divider which makes possible different impedance pick-up settings. An adjustable phase-shifting circuit also is provided to allow the point of maximum reach on the mho characteristic (Figure 11) to occur at different angles.

Figure 7. Thyratron pilot wire relay

The tube circuit is the sensitive element which is responsive to signals from both the measuring circuit and the pulsing circuit. The tube circuit also initiates the tripping signal to the circuit breaker when the impedance being measured falls below that for which the relay has been set to operate.

Figure 12 shows graphically the line voltage V, the line current I, and related quantities as applied to a typical unit during fault conditions. The pulse (V_p) as generated by the pulsing circuit is seen to occur at the moment of line voltage maximum. The line current (I) is passed through the primary of the transactor in the measuring circuit. A transactor is an air-gap transformer and has a secondary voltage proportional to the primary current. Normally, this secondary voltage leads the primary current by 90 degrees. Actually, it is desirable that this angle be made adjustable by means of a phase-shifting circuit. The dotted wave in Figure 12 represents the shifted secondary transactor voltage. The polarity of the transactor is reversed so that the direction of its secondary voltage will be opposite to that of the line voltage.

In the measuring circuit the line voltage (V) and the voltage proportional to the line current (V_I) are opposed and the combination placed on the grid of a tube in the tube circuit. The direction of the voltage is such that it tends to hold the tube off at the measuring moment (instant of line-voltage maximum) while the direction of the voltage proportional to the line current is such that it tends to cause the tube to conduct at the measuring moment. The line voltage could be considered to have a restraining effect, while the current has an operating effect. A bias in this series circuit is adjusted so that at the measuring moment the tube will conduct when the voltage proportional to the line current just equals the line voltage.

In the Appendix, it is shown mathematically that the arrangement described will have a mho-type operating characteristic. It also should be pointed out that, to build a unit with a reactance characteristic (Figure 13), similar basic elements are used, that is, a pulsing circuit, a measuring circuit, and a tube circuit. The difference is that the measuring moment occurs at the time of the line-current zero. This is also shown mathematically in Appendix A.

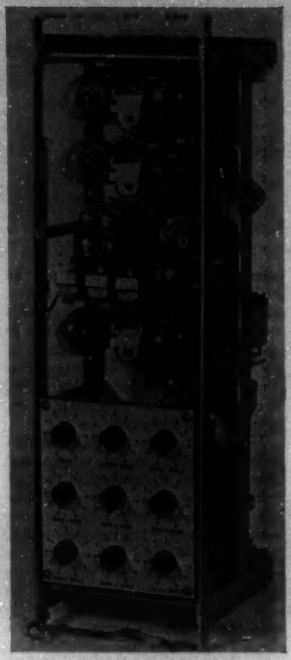

Figure 8. Sample electronic mho relay

Referring again to Figure 12, it can be seen that the current will have its greatest effect when the power-factor angle is equal to the angle between the voltage V_I and the line current I since for this condition V_I is a maximum at the measuring moment. This angle then is the angle of maximum reach on the mho characteristic.

Operating Time

Owing to inertia, measurements made by electromagnetic relays must be made on a rms basis. In electronic relays, on the other hand, no inertia is present and

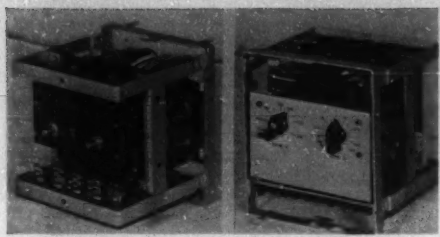

Figure 9 (left). Sample electronic timing relay

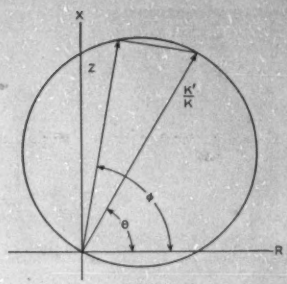

Figure 11 (right). Mho characteristic on R-X diagram

measurements may be made on an instantaneous basis. The use of instantaneous values for measurement makes the electronic relay inherently faster in operating time. The electromagnetic relay must integrate the quantities being compared over a period of time before it can determine the magnitude of the quantity being measured.

As the electronic mho relay compares instantaneous values of voltage and current only at the moment of the line-voltage maximum, the operating time will depend on the moment in the cycle the fault occurs. This may be understood by considering a fault which occurs at some time after the line voltage maximum. The relay must wait until the next line voltage maximum occurs before measurement can be made. If only a single tube is used, the operating time will vary from 0 to 1 cycle plus the operating time of an auxiliary tripping relay. On the other hand, the use of two tubes reduces the operating time from 0 to 1/2 cycle plus the operating time of an auxiliary tripping relay. One tube would be made conductive at the moment of positive half cycle line voltage maximum and the other at the moment of the negative half cycle line voltage maximum. Each would have the proper signal from the measuring circuit on its control grid. The operating time is independent of the impedance the relay sees, expressed as a percentage of the pickup setting. This is illustrated in Figure 14.

Burdens

As can be expected with electronic devices, the burdens of the electronic mho relay are very low. Each current circuit burden is less than 2.5 volt-amperes at 5 amperes, and the potential circuit burden is less than 1.5 volt-amperes at 115 volts.

Effect of Transients

Transient shunts are provided to eliminate the d-c component of the fault current wave. Unless the transient shunt is used there is a tendency for the relay to overreach owing to the instantaneous nature of the measurements. When a single tube is used without the transient shunt and a fault occurs near the end of the protected section during the positive half cycle of the line voltage, there will be negative offset and tripping will be delayed until the offset portion of the current has decayed.

An Electronic Timing Relay

The two time steps required for the 3-zone electronic mho relay are furnished by an external electronic timing unit. These time steps are independently adjustable over a range of 0.05 to 3.5 seconds.

The time delay is obtained by means of the well-known RC decay circuit; a capacitor in the grid circuit of a tube normally is charged negatively to a certain fraction of the d-c supply voltage by means of a potentiometer connected across the d-c supply. This setting controls the operating time because the capacitor discharges through a resistor until it reaches the point where its negative voltage is reduced sufficiently to permit the tube to fire.

An important feature of this timing unit is that, unlike most devices using an *RC* timing circuit, its connections are such as to make the operating time practically independent of the variation of the d-c supply voltage.

Conclusions

Before electronic relays can be accepted in the protective relay field, they must be at least as good as electromagnetic relays in all respects and superior from the standpoint of one or more of the following: reliability, speed, accuracy, burden, and maintenance. Their application also is justified where they can be made to perform functions not possible, or not economically feasible, with electromagnetic relays.

It has been found possible to build a typical high-accuracy electronic protective relay which, laboratory tests indicate, is in every way as good as its electromagnetic counterpart, and in addition, provides faster and more consistent operating time, greater sensitivity, and lower burden. Laboratory tests, however, cannot be considered as the final measure of performance in actual service. Accordingly, plans for comprehensive operating tests on a power system currently are being carried out and, if these tests prove successful, the relay will be made available for applications where its superior speed, sensitivity, and burden

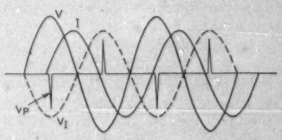

Figure 12. Currents and voltages applied to mho unit

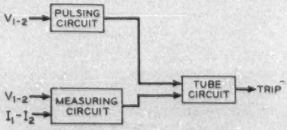

Figure 10. Block diagram showing arrangement of electronic distance relays

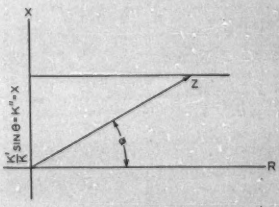

Figure 13. Reactance characteristic on R-X diagram

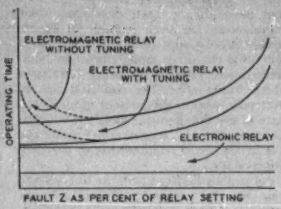

Figure 14. Time characteristics of electromagnetic and electronic relays

characteristics are considered advantageous.

In the meantime, development work will continue in order to explore the further possibilities of electronic relays so that when there are applications requiring features which can be obtained better with electronic relays than with electromagnetic relays, the industry will find that they will be available.

Appendix

The operation of the electronic mho relay may be considered mathematically as follows.

Expressions for instantaneous values of the quantities to be compared are

$$e = E_{max} \sin wt \tag{1}$$

and

$$i = I_{max} \sin (wt + \phi) \tag{2}$$

where e is the instantaneous value of line voltage, E_{max} is the peak value of line voltage, i is the instantaneous value of line current, I_{max} is the peak value of line current, ϕ is the power factor angle, wt is the angular velocity in radians per second of the alternating current, and t is the time in seconds from a zero point on the voltage wave. In the relay the current is further shifted by an angle θ and both current and voltage are modified by constants K' and K respectively. The bias on the control grid of the relay tube is such that it will conduct when the voltage proportional to the line current $[K'I_{max} \sin (wt+\phi-\theta)]$ just exceeds the line voltage as modified by the relay $(KE_{max} \sin wt)$, that is, the threshold value is given by

$$KE_{max} \sin wt = K'I_{max} \sin (wt+\phi-\theta) \tag{3}$$

Expanding the right-hand side of equation 3

$$KE_{max} \sin wt = K'I_{max} [\sin wt \cos (\phi-\theta) + \cos wt \sin (\phi-\theta)] \tag{4}$$

Considering the moment of the line voltage maximum where measuring is to take place,

$$wt = 90 \text{ degrees} \tag{5}$$

Substituting in equation 4,

$$KE_{max} = K'I \cos (\phi-\theta) \tag{6}$$

$$Z = \frac{K'}{K} \cos (\phi-\theta) \tag{7}$$

which is the polar equation of a mho circle with diameter equal to K'/K. (See Figure 11.)

It has been stated the relay described in this paper may be made to have a reactance characteristic by causing the measuring moment to occur at the instant of line current zero. To show that this arrangement produces a reactance characteristic consider equation 2. The line current will be zero when

$$wt + \phi = 0$$

or

$$wt = -\phi \tag{8}$$

and substituting in equation 3

$$-KE_{max} \sin \phi = -K'I_{max} \sin \theta \tag{9}$$

or

$$Z \sin \phi = \frac{K'}{K} \sin \theta = K'' \tag{10}$$

since θ is the relay angle and is constant.

Since $Z \sin \phi = X$,

$$X = K'' \tag{11}$$

which shows that a unit so arranged would have a pickup at a constant value of reactance.

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Discussion

Archie W. Adams (Bonneville Power Administration, Portland, Ore.): The development work on electronic relays outlined in this paper appears to be an important step in obtaining higher speed relaying. In order to adequately handle load increases, it is necessary to raise the power capabilities of transmission lines. Modern oil circuit breakers are available to open in less than 3 cycles and with reclosing times of 20 cycles or less. In order to obtain the maximum advantage of these circuit breakers, relay operating times should be reduced to less than 1 cycle.

Stability studies on the Bonneville Power Administration system indicate a definite need for high speed and simultaneous operation of the relays at both ends of the line in order that we may utilize fast automatic reclosing of the circuit breakers.

We have recently made some preliminary tests on the deionization times for high-voltage arcs. These results appear to indicate that regardless of the magnitude of fault current the deionization time is less for short duration fault currents. From this standpoint, fast relaying is necessary in order to obtain faster reclosing times. This is in addition to the obvious advantage of reducing system shock by keeping the duration of fault current to a minimum value.

The Bonneville system has not been able to obtain satisfactory speed of relaying using the conventional distance-type relays. The principal reason for the present comparatively slow relay time is due to the low operating torques of the relay elements and an operating point for far end faults near the pickup value of the relay. The low operating torques are the result of fault currents from one end of the line that are of the same relative magnitude as load currents. In spite of the heavy faults on the Bonneville-Grand Coulee transmission system, the line lengths are such that, until the first circuit breaker has opened, the fault current from the far end of the line is small. We have found it necessary to set the carrier relay zone so that it extends not more than 15 or 20 per cent beyond the line section in order to minimize relay operations during system swings. This setting results in slow operating time for certain fault locations. An alternate solution would be to use "blinders" and extend the relay setting. Up to the present time, we have not applied these "blinders" because of the desire to keep our relaying as simple as possible.

The electronic mho relay outlined in this paper with high-speed operation up to the pickup setting of the relay should considerably shorten relay operating times which we have been able to obtain from conventional relays.

On the basis of our experience with present relays and our stability studies which show the improved system performance to be expected with faster relaying and high-speed reclosing, we believe that the manufacturers should expedite this type of relay development in order to produce the most reliable, simple, and fastest relay system possible.

C. E. Asbury (The Commonwealth and Southern Corporation, Jackson, Mich.): It is interesting to see further investigation and development work being carried on to

improve the reliability and performance of protective relaying. Because of the generally accepted use of carrier current relaying and the development of better electronic tubes, it appears to be timely that the possibilities for protective relay development in the field of electronics be explored further.

One of the limitations to the application of carrier current relaying is its complexity, not only from the standpoint of installation, but also from an operation and maintenance point of view. The present day cost of carrier current relaying also limits its application in many instances. Most of this cost is in the electronic equipment, coupling capacitors, and wave traps making up the carrier channel rather than the cost of the magnetic relays controlling this channel.

Since the component parts of electronic equipment such as tubes, condensers, resistors and sockets, have already been developed and are in mass production for other uses, it would appear that the manufacture of electronic relays could be reduced to an assembly process once a design is established and such products placed on the market at a much lower cost to the customer than the equivalent magnetic relay requiring special manufacturing processes for a number of special parts in rather small quantities.

In comparing electronic relays with magnetic relays not only should the reliability, speed, accuracy, burden, and maintenance, as mentioned by the authors, be considered, but also the cost, complexity, and space requirements should be considered. It would be interesting to know the space requirements for the new electronic mho relay as compared with that required for the magnetic mho relay.

One of the problems encountered with directional phase relays is the necessity of providing a voltage so that directional discrimination can be obtained during fault conditions when the voltage polarizing source may be quite low. In some relays this is done by using voltage insofar as possible from unfaulted phases. These arrangements are still subject to possible incorrect directional indication for close-in 3-phase faults. In the magnetic design mho relay, use is made of a tuned circuit having time delay to overcome these difficulties. This is referred to as "memory action." Is this memory action feature provided in the electronic relay and if so, how is it accomplished?

The authors explain in the paper that the accuracy of the electronic relay will be better than that of the magnetic relay. The present recommended practice for setting zone 1 of the magnetic relay is to cover 90 per cent of the line section. With the new electronic relay will the accuracy and consistency be sufficient to increase the reach of zone 1 to cover more than 90 per cent of the line section?

Will it be possible to use the new electronic mho relay to control carrier for carrier relay applications? It would be well if the authors would point out how such control is effected; whether it is accomplished by contacts in the carrier control circuit or if it has been possible to effect control by means of biasing control tube circuits in the carrier transmitter by the electronic relay.

R. H. Macpherson: Asbury has pointed out that, since the components of electronic relays have already been developed, their manufacture has been reduced to an assembly process with resultant reduction in cost. On the other hand, since new components are continually being developed which would affect the cost and size of electronic relays, the present preliminary state of the development makes it impossible to predict with any certainty either the cost or the space requirements of the electronic relays.

For nearby faults, where the line voltage may drop to a very low value, memory action is provided in the pulsing circuit. The input of a pulse producing transformer is tuned so that the pulse will continue to occur several cycles after the inception of the fault, thus allowing adequate time for the relay to operate.

The accuracy of the electronic mho relay is not necessarily better than that of the electromagnetic mho relay. This is due to the yet undetermined effect of system transients which may cause false operation unless they can be removed from the measuring circuit. This will be studied in field tests.

No attempt has yet been made to apply the electronic mho relay with carrier-current relaying. It is hoped that, when the application can be made, control will be effected without relay contacts since this would eliminate relay operating time.

Adams called attention to the advantages of consistently fast tripping in connection with high-speed reclosing. This will undoubtedly be realized if the results of the field tests do not dictate the necessity for time delay to obviate errors that might be caused by the system transients referred to previously.

Instantaneous Bus-Differential Protection Using Bushing Current Transformers

H. T. SEELEY
ASSOCIATE AIEE

FRANK VON ROESCHLAUB
MEMBER AIEE

DUE to ever higher standards of service reliability and decreased margin of spare generating capacity, the problem of differential protection for station busses has received considerable attention[1] in the last decade. Installations have been made with simple low-impedance unrestrained instantaneous overcurrent relays fed from conventional current transformers and set for a small fraction of the maximum fault current. The test data included here show that such installations are not safe for a high standard of service reliability because they will trip if residual magnetism and the d-c component of offset current cooperate sufficiently to approach the maximum error current. This problem of d-c saturation of the current transformer cores has led to various unsatisfactory expedients such as 600-pound current transformers[2] or the compromise of making careful calculations[3] of the settings of induction relays for the sake of using standard current transformers even where instantaneous relays would be preferred. This problem has already been solved by direct attack, eliminating part[4] or all[5] of the iron in the magnetic circuit, but each of these systems involves an increase in cost for the mutual reactor that replaces the conventional current transformer, and neither one is readily applicable to existing installations.

A simple differentially-connected relay with a series stabilizing resistor has been used with some success[6,7,8] and has been mentioned in the literature. The scheme as previously applied has been limited in sensitivity by the secondary voltage that can build up across such a stabilizing resistor in the differential circuit on internal faults. The scheme has been investigated analytically,[9] but the results covered too few cases to serve as a general basis for choosing the relay settings and resistance values. Furthermore, no general calculation method has been published to establish the range of usefulness of the scheme. This paper presents a protection system that is an extension of the stabilizing-resistor idea, including the development of a relay that circumvents the limitation of high voltages on internal faults and fully discussing the manner of application for a wide range of current transformers.

Operating Principle

The high impedance of the relay in comparison to the resistance of the current transformer secondaries accounts for the successful operation of this system. To understand how this system provides tripping on internal faults and freedom from false tripping on external faults when one or more of the current transformers is saturated, it is necessary to consider the voltage that appears under extreme conditions at the junction points where the current transformers are connected in parallel and where the high-impedance-relay leads are also connected. See Figure 1.

External Faults

Under external-fault conditions if the current transformers perform ideally, the current in the secondary of the current transformer on the faulted line ("fault CT") is equal and opposite to the vectorial sum of the currents in the secondaries of the remaining current transformers. The voltage at the junction points is zero, and no current flows in the relay, whether it is of high or low impedance. If the "fault CT" saturates, and none of the other current transformers suffer a reduction in ratio, the same secondary current must flow in the secondary of the "fault CT" if the relay impedance is infinite. This secondary current, equal to the sum of the secondary currents in the other current transformers is, however, forced through the "fault-CT" branch impedance by the development of a voltage across the junction points (error voltage) equal to the product of the current and the impedance encountered. This impedance may have three components as follows:

1. Magnetizing impedance of the "fault CT." This is cancelled out by that portion of total primary ampere-turns which balances the secondary ampere-turns. Any excess of primary ampere-turns generates a voltage in the opposite direction to the

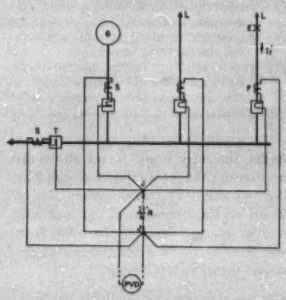

Figure 1. Bus section with differentially-connected high-impedance relay

E—External fault
F—"Fault CT"
G—Generator
J—Junction point
L—Radial lines without generation
PVD—High impedance relay
S—"Source CT"
T—Tie circuit breaker to another bus section

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H. T. SEELEY is with the General Electric Company, Philadelphia, Pa., and FRANK VON ROESCHLAUB is with Ebasco Services Inc., New York, N. Y.

Table I. External Fault. Voltage Across High-Impedance Relay with Residual Flux and Fully-Offset External-Fault Current

Current Transformers			Calculated 2-Way Lead Resistance, Ohms	Measured Total Branch Resistance, Ohms	Offset and Residual Additive or Subtractive in "Fault CT"	"Fault-CT" Secondary Amperes		Relay Voltage				Oscillogram Number See Figure 9
									Offset Crest			
Number of "Source CTs"	Rating	Measured Resistance, Ohms				Calculated Rms* Symmetrical I_f'/N	Crest Measured Amperes	Measured Voltage	Per Cent of $\frac{2\sqrt{2}RI_f'}{N}$	Equivalent **		
4	69 kv	0.40	1.29	1.69	Additive	130	219	470	76	47		77
4	600/5 amperes	0.40	0.63	1.03	Additive	120	231	320	91	33		76
8	15 kv	0.81	4.04	4.85	Additive	101	190	1,100	79	110		132
8	2,000/5 amperes	0.81	1.48	2.29	Additive	99.5	230	710	110	82		131
8		0.81	1.49	2.30	Subtractive	87.1	230	250	44	†		103

* These are calculated as $\frac{\text{measured crest of primary current}}{2\sqrt{2}\ \text{(turn ratio of setup)}}$

** Calculated from average value of highest cycle of relay current.

† This "Equivalent Voltage" value is not significant because the test was made with a series capacitor instead of a resonant impedance.

impedance drop, thereby reducing the error voltage.

2. Secondary leakage reactance. This is quite negligible[10] on the full winding and some taps of bushing-type current transformers; this system should not be used on partial windings without special consideration. Reactance of secondary leads is likewise negligible.

3. Secondary branch resistance equal to the sum of the current transformer secondary winding resistance and resistance of the leads to the junction points. This total resistance is encountered by the secondary current.

Therefore, as shown in the Appendix, the maximum crest voltage that may appear across the junction point is

$$\frac{2\sqrt{2}RI_f'}{N}$$

where

I_f' = maximum external fault current, rms symmetrical

N = number of secondary turns in each bushing current transformer

R = branch resistance for the current transformer having the longest leads to the junction point, winding plus leads

To avoid false tripping on external faults, the relay would be set above this maximum voltage; a safety factor of 2 has been chosen. The rms voltage RI_f'/N based on the symmetrical value of fault current is used, because the relay is insensitive to the d-c component as discussed under Relay Design.

Internal Faults

Under internal fault conditions, the voltage across the high-impedance relay at the junction points approaches the open-secondary voltage that the current transformers will produce. Since the peaks of this voltage wave are quite high, they are in practice limited to a safe value by a Thyrite resistor across the relay, that establishes a voltage ceiling. For successful application, the pick-up setting of the relay must necessarily be below the junction-point voltage produced by minimum internal-fault current. This in general is in excess of the value RI_f'/N, and therefore, the relay, which is set to respond to voltage at its terminals, can distinguish readily between internal and external faults.

Low-Impedance Relay

An analysis[11] has been made of a protective system operating under the same conditions except for the use of a conventional low-impedance relay, and this shows that on external fault where the "fault CT" saturates completely, the relay current can reach 94 per cent of the sum of the secondary currents in the remaining current transformers, since the relay impedance is low in comparison to the secondary resistance of the saturated "fault CT." Thus the low-impedance-relay current is of the same order of magnitude whether the fault is internal or external when saturation occurs, and discrimination between these two types of fault is not readily possible.

One might easily, but incorrectly, assume that for the same external fault the maximum value of error voltage across a high-impedance relay would be that read from the excitation curve, corresponding to this same high value of error current. Fortunately the "fault-CT" circuit acts as a shunt and limits the error voltage to the value RI_f'/N given before.

Additional Margin

The foregoing analysis is based on assumptions that give the limiting condition —that is, the voltage RI_f'/N is the maximum that can be effective on the relay for external faults. However, saturation in general does not exist throughout the entire current cycle, but only on approximately every alternate half cycle. Secondary voltage is thus generated, during the nonsaturated half cycles, that reduces the effective voltage to which the relay responds.

If, as is the case practically, current can flow in the relay, or if any of the current transformers on source connections to the bus ("source CTs") suffer a reduction in effective ratio, the amount of secondary current forced through the saturated current transformer is reduced, and the voltage at the junction points will be less than RI_f'/N, as shown in the Appendix.

With this system, the "source-CT" cores saturate quickly and this greatly reduces the time that the relay must withstand the high error voltage that results from "fault-CT" core saturation in the absence of "source-CT" core saturation. The reason for this quick satura-

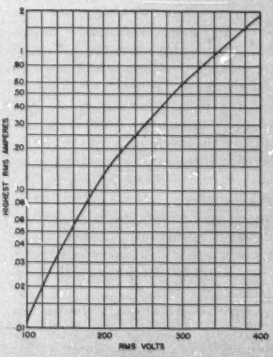

Figure 2. Volt-ampere characteristic of Thyrite resistor of bus-differential relay with taps set for four disks. Highest refers to manufacturing variations

tion of the "source-CT" cores, in contrast to their relatively slow saturation with a low-impedance relay, is that with the high-impedance relay the "source CTs" must force nearly all their output current through the "fault-CT" branch, and with offset currents this causes a d-c component, $(\sqrt{2}\ RI_f'/N)\epsilon^{-(t/T)}$ of voltage to appear across the junction points as soon as the "fault-CT" core reaches saturation. This direct voltage required from the "source CTs" saturates all their cores in a subsequent time interval of the same order as the one required for saturating the "fault-CT" core.

These phenomena all produce a margin of safe operation beyond that determined by the value RI_f'/N. Since it is not practical to calculate these phenomena, their over-all effect has been determined by extensive tests made on bushing current transformers under a sufficiently wide variety of conditions to permit interpolation for all the bushing current transformers used in the manufacturer's line of switchgear. The factors thus determined, together with the safety factor of 2, were used in establishing the values of pick-up setting given in Table V.

It is evident from the foregoing that the branch resistance R of the current transformer secondary circuits must be kept to minimum values if the greatest sensitivity of the system is to be attained. In many cases, however, no special precautions will be found necessary in this respect, and in some cases it may be found possible to include additional devices such as ammeters or partial-differential back-up relays. The outstanding advantage of this differential system is that these possibilities are all readily predictable, and the operation of the differential relay is subject to straightforward calculation.

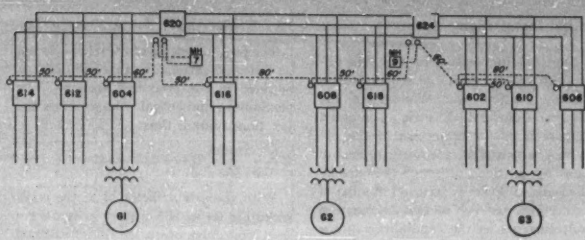

Figure 3. 3-section 60-kv bus at Ripley Station, showing bus-differential bushing current transformer secondary lead runs

G1—generator number 1, 28 750 kva
G2—generator number 2, 31 250 kva
G3—generator number 3, 35 294 kva
MH7, MH9: Manholes through which leads are carried back to relays on main control switchboard
Approximate lead lengths between circuit breaker housing terminal blocks for bushing current transformer leads are indicated in feet
Circuit breakers 614, 612, 616, 618, 602 and 607 control lines to substations all capable of backfeed

Sensitivity

The safe pick-up setting for the relay having been established in terms of the maximum external fault current, the sensitivity of the protection afforded for low internal fault currents is determined readily by considering the excitation requirements of the current transformers. The primary current must supply the following:

$$\text{Relay pick-up current} = \frac{\text{pick-up voltage}}{\text{relay resistance}} N = I_{rp}N$$

$$\text{Thyrite current} = I_{cp}N$$

$$\text{Excitation current for each current transformer at pick-up voltage} = I_{ep}N$$

The relay resistance is 3,200 ohms. The Thyrite current can be calculated from Figure 2, on the basis of four disks for preliminary purposes. The excitation curves[13] are available from the manufacturer, in accordance with the ASA Standard C57.1 adopted in 1942.[13]

Total primary current required for pickup with n current transformers per phase in the differential circuit is

$$I_p'_{\min} = (I_{rp}+I_{cp}+nI_{ep})N$$

where, for the sake of simplicity the pessimistic assumption is made that the currents can be added as scalar quantities. This is the minimum internal fault current that will be detected by the relay.

Table II. External Fault. Current Through Low-Impedance Relay with Residual Flux and Fully-Offset External-Fault Current, Using Eight "Source CTs" Rated 15 Kv, 2,000/5 Amperes, 0.81 Ohm

Calculated 2-Way Lead Resistance, Ohms	Measured Total Branch Resistance, Ohms	Offset and Residual Additive or Subtractive in "Fault CT"	"Fault-CT" Secondary Amperes: Calculated Rms* Symmetrical I_f'/N	"Fault-CT" Secondary Amperes: Offset Crest Measured Amperes	Relay Current: Measured Amperes	Relay Current: Offset Crest: Per Cent of $2\sqrt{2}I_f'/N$	Relay Current: Rms** (approx.) First Cycle	Current Relay Setting: Trip	Current Relay Setting: Nontrip	Oscillogram Number See Figure 9
4.04	4.85	Additive	102	84	280	97	170		***	138
1.49	2.30	Additive	102	130	270	93	160		***	137
1.47	2.28	Subtractive	103	250	170	58	104	96	96†	144
0.03	0.84	Additive	104	200	200‡	68	120		***	145
8.89	9.70	Additive	59.5	40	160	95	94		***	150
3.99	4.80	Additive	60.3	60	160	94	97		***	151
0.03	0.84	Additive	59.5	130	110§	65	68		96−	152
0.03	0.84	Additive	59.5					70+		

* Calculated as for Table I.

** These are $\frac{\sqrt{3}\ (\text{measured crest of relay current})}{2\sqrt{2}}$ which is the rms value of a fully-offset sine wave with infinite time constant.

*** The maximum available setting on the relay was 96 amperes; the relay tripped at this setting.

† This setting barely tripped on one test and almost tripped on another.

‡ This crest differential current occurs 1/4 cycle after the crest of the "fault-CT" secondary current.

§ This crest differential current occurs 7/8 cycle after the crest of the "fault-CT" secondary current.

Initial Installation

The first installation of this bus differential scheme is at the Ripley Steam Electric Station near Wichita, of the Kansas Gas and Electric Company. Requirements for protection of the 60-kv bus at this plant are fairly typical of many applications, although they are by no means so severe in terms of number of connections per bus section or range of fault currents as the capabilities of the new relay system encompass.

Data on Installation

A diagram of the bus is shown in Figure 3, together with approximated resistances of the leads to the various current transformers in the circuit breakers from the junction points.

All circuit breakers on the bus have General Electric 600/5 ampere type *BR* bushing current transformers (BCT) available for use solely in the bus differential circuit, as shown in Figure 3.

Minimum anticipated fault on bus	700 amperes
Maximum external fault current (symmetrical value)	13,400 amperes
Resistance of leads from junction points (circuit breaker 620) to most remote bushing current transformer (circuit breaker 624)	0.43 ohm, 25 degree centigrade 0.49 ohm, 60 degree centigrade
Resistance of bushing current transformer secondary including leads to terminal block in circuit breaker	0.53 ohm, 25 degree centigrade 0.69 ohm, 95 degree centigrade
Excitation characteristic of bushing current transformer	Figure 4

Pick-Up Setting

The pick-up setting of the relay is based on extreme external fault conditions as outlined in the preceding section. The maximum symmetrical voltage across the junction points is thus:

$$R\frac{I_f'}{N}=\frac{13{,}400}{120}(0.49+0.69)=132 \text{ volts}$$

With the safety factor of 2, the relay pickup is set at 265 volts to assure freedom from false operation under extreme external fault conditions.

Sensitivity

The sensitivity of protection afforded for internal faults is determined as described in the previous section. The middle bus section is considered, since, having five connections, it will give the lowest sensitivity

$$\begin{aligned} I_{rp} &= 265/3{,}200 = 0.083 \\ I_{cp} &= 0.367 \\ nI_{ep} &= 5\times 0.33 = 1.65 \\ \hline \text{Total secondary current} &= 2.10 \end{aligned}$$

The total primary current required is therefore $120 \times 2.10 = 252$. This value is sufficiently below the anticipated minimum fault of 700 amperes to assure ample protection.

The maximum number of connections on the bus section that could be accommodated within the desired limits of sensitivity is

$$n_{max}=\frac{I_{pmin}-(I_{rp}+I_{cp})}{I_{ep}}=\frac{(700/120)-(0.083+0.367)}{0.33}=16$$

Thus the possibility of providing protection for any of these bus sections is quite adequate.

The foregoing calculations for this application are based on the theoretical limiting conditions. Tests indicate that for these conditions the additional margins discussed under Operating Principles provide a factor of 2 for these current transformers—that is, the ratio $(RI_f'/N)/(\text{equivalent relay voltage})=2$. This and corresponding factors for other conditions are used in Table V. To take advantage of this margin and obtain increased sensitivity, the pick-up setting could be made $265/2 = 130$ volts and still retain the original safety factor of 2 between pickup and anticipated maximum relay voltage on external fault.

Relay Design

The design of a relay to operate properly in the differential scheme involves certain requirements in addition to high impedance. When offset fault currents occur, or residual magnetism exists in the current transformer core, or both, an appreciable d-c component in the secondary current is present, causing direct voltage to appear across the relay in addition to the alternating component calculated from the maximum external fault current and current transformer secondary and lead resistance. It is desirable to prevent this d-c component from causing the relay to pick up, without reducing the sensitivity of the system to the a-c component of minimum internal fault current. This is accomplished by connecting the relay element in series with a capacitor and suitable reactor such that the entire combination is resonant at the fundamental power frequency; the d-c component of secondary current is blocked from passing through this relay circuit by the series capacitor. Thus the impedance of this circuit is made high, and essentially resistive.

The resonant circuit was not adopted for the purpose of riding over the transient error voltage produced by the saturated current transformers, but solely to block the d-c component of voltage, and may thus be likened to the circuits used in connection with distance relays to avoid over-reach on offset fault currents.

Table III. External Fault. Current Through Low-Impedance Relay with Nonlinear Limiter (Thyrite) Resistor, with Residual Flux and Fully-Offset External-Fault Current Additive in "Fault CT," Using Eight "Source CTs" Rated 15 Kv, 2,000/5 Amperes, 0.81 Ohms

Calculated 2-Way Lead Resistance, Ohms	Measured Total Branch Resistance, Ohms	"Fault CT" Secondary Amperes: Calculated Rms* Symmetrical I_f'/N	"Fault CT" Secondary Amperes: Crest Measured Amperes	Thyrite Resistor: Disks	Thyrite Resistor: Crest Measured Voltage	Thyrite Resistor: Crest Measured Amperes	Relay Setting: Trip	Relay Setting: Nontrip	Oscillogram Number See Figure 9
1.49	2.3	59.3	140	1	300	20		13	142
1.49	2.3	59.3	**	1	**	**	10		
1.49	2.3	59.3	**	2	**	**		4	
0.05	0.86	60.1	150	1	160	3—		4	143
0.91	1.72	176	300	2	600	31		8	163

* Calculated as in Table I.

** Not measured.

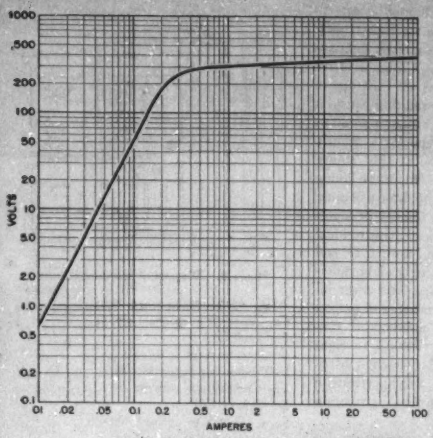

Figure 4 (left). Secondary excitation characteristics for General Electric bushing current transformer, type BR, 600/5 amperes for 69-kv circuit breaker

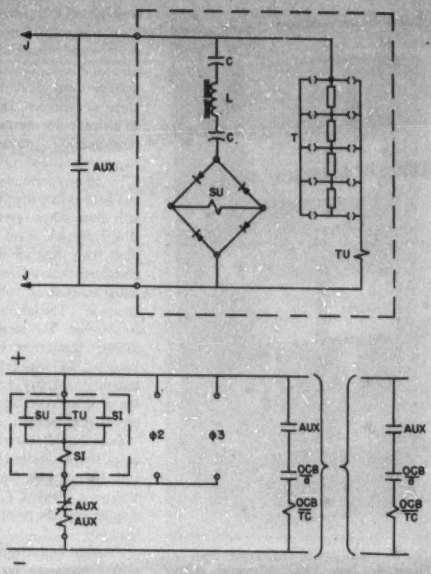

Figure 5. Circuits for protective and auxiliary relays. Elements of PVD bus-differential relay for phase 1 are shown within broken-line rectangles

C—Capacitors in resonant circuit
L—Reactor in resonant circuit
T—Thyrite resistor and tap blocks
SU—Sensitive unit
TU—Thyrite unit
SI—Seal-in and target unit
AUX—Auxiliary tripping relay
OCB—All circuit breakers on bus section protected
J—Leads to secondary junction points in bus run

In order to provide resistance to closure on shock, and to take advantage of the reasonably high power levels available to the sensitive unit, it was decided to employ a robust plunger-type device. Since the inductance of such a relay varies with the plunger position which determines the pick-up setting, the coil is energized through a rectifier to prevent upsetting the resonance of the entire relay circuit when the plunger is moved.

The Thyrite resistor connected in parallel with the circuit of the sensitive unit consists of a tapped stack of disks. The tap setting is chosen so that the resistor draws only a small current at the pick-up setting of the sensitive unit. For high internal faults that tend to produce high voltages in the secondary circuit, the Thyrite draws high current, thereby establishing an effective ceiling on the voltage that can be attained across the junction points. This ceiling is in general set at or below a peak voltage corresponding to 1,500 volts rms, which is considered allowable momentarily in switchgear secondary wiring. It should be noted that this ceiling voltage lasts only for the short interval required to pick up the relay on an internal fault and operate the auxiliary tripping relay that short circuits the secondaries of the current transformers as shown in Figure 5.

The presence of the Thyrite resistor together with the resonant characteristic of the sensitive-unit circuit has a tendency to slow the operation of this element to as much as 3 to 6 cycles by retarding the current build-up in it. In order to improve the tripping speeds of the entire system on high internal faults, an additional relay unit is connected in series with the Thyrite, operating on the current shunted through this circuit. Thus shorter tripping times for such faults are obtained, in the range of 1 to 3 cycles. Tests have been made to permit a choice of setting for this Thyrite relay unit that is safe against false tripping on the most adverse conditions of offset current and residual magnetism combined.

Figure 6 shows the complete relay assembly.

Test Procedure

Since the tests were made for determining the maximum value of error voltage, it was necessary to set up the most unfavorable conditions.

Offset currents are commonly recognized as the cause of current transformer errors, so these were provided by using: a synchronous switch to control the starting phase angle to give full offset; a 5-ton iron-core reactor with air gap to give a long time constant; and a stacked multiturn primary of copper bars to keep the current requirements below 10 per cent of station rating thus minimizing the effect of the resistance of the supply circuit and the station transformers on the time constant. Figure 7 shows the general arrangement of apparatus for testing one "fault CT" with four other current transformers.

It has already been shown[14] that residual flux from a previous fault can remain at a high value, and the wide use of instantaneous reclosing necessitates provision for the full value, which may be as high as 80 kilolines per square inch. Therefore the test circuit, Figure 8, was adapted to saturate all the cores in the same direction with direct current before each test. Since nearly two-thirds of the flux capacity of the "fault-CT" core was used by this residual flux following full direct current saturation, primary waves having only one-third the time constant that would otherwise be necessary were adequate to complete the saturation of the core.

Figure 6. Type PVD differential voltage relay

87L—Sensitive unit
87H—Thyrite unit

During external faults, the current in the "fault CT" is in the opposite direction from that in the remaining "CTs," so the residual can be additive in the "fault CT" and subtractive in the others. This is the worst condition, and the use of the synchronous switch made it possible to insure this relation.

In the application of multirestraint relays the current distribution has been an important consideration. With the simple high-impedance differential relay, a uniform current distribution among all the "source CTs" gives the highest error voltage and therefore the most unfavorable condition, because it minimizes the voltage drop (RI_f'/Nm) in each one of these m "source CTs" and thus causes it to maintain its normal output current at more nearly full value and for a slightly longer time. Saturation of the "source CTs" in any form of differential relaying is a help rather than a hindrance because it is always less severe (except for a possible half-cycle effect of additive residual flux) than that of the "fault CT" and therefore it can only reduce the false differential current or voltage, but never increase it. The largest number of sources is therefore the most unfavorable condition; tests were carried up to eight "source CTs" with primary-current distribution 2, 2, 2, 2, 1, 1, 1, 1 to approximate the most unfavorable current distribution expected in service.

Each sample current transformer of a given size was tested for exciting current, and the one with the highest value was used as a "fault CT" in setting up that size, to give the most unfavorable condition.

External-fault tests to determine the setting necessary for avoidance of false tripping, and internal-fault tests to check the crest voltage on the insulation, were carried to 160 amperes secondary as compared with the limit of 100 amperes often taken as the basis for choosing current-transformer ratios for differential protection. Various measured values of lead resistance were tested to provide data for the expected range of application conditions.

Test Results

Table I and oscillograms 76, 103, and 131 of Figure 9 give typical data from the tests that were made for the purpose of checking the theory and determining the safe settings of the relay. Lines 1 and 2 cover the 69-kv 600/5 ampere current transformers with preliminary values of lead resistances, to be used at the Ripley Station mentioned before. Although for the current level and lead resistance expected in service, the crest value of error voltage is 76 per cent of the predicted maximum, the effect on the resonant circuit is only about 10 per cent of the predicted crest value $2\sqrt{2}\ RI_f'/N$ on the basis of a fully-offset wave with infinite time constant. This extra margin as compared with the predicted poorest performance of these current transformers with, for example, an electronic relay responding to instantaneous values, has already been explained under the sections on Operating Principles and Relay Design.

The high-current tests were made with a time constant of about 0.05 second, and the medium- and low-current tests at 0.11 to 0.15 second. As will be seen from the oscillograms, either of these time constants gives quick saturation of the "source-CT" cores as shown by the fact that the half cycles of secondary current in the offset direction are greatly diminished; so a change in time constant would only change the time required for recovery but would not change either the crest or equivalent value of error voltage to any great extent. The reason for this is that the use of opposite premagnetiza-

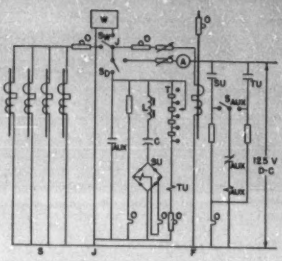

Figure 8. Diagram of test connections

AUX—Hand-reset auxiliary relay
C—Capacitor in resonant circuit
F—"Fault CT"
J—Junction point
L—Reactor in resonant circuit
O—Oscillograph element
S—"Source CT"
S_{AUX}—Switch to connect AUX to SU or TU
S_D—Switch for d-c saturation
S_W—Switch for Wheatstone bridge
SU—Sensitive unit
T—Thyrite resistor stack with taps
TU—Thyrite unit
W—Wheatstone bridge

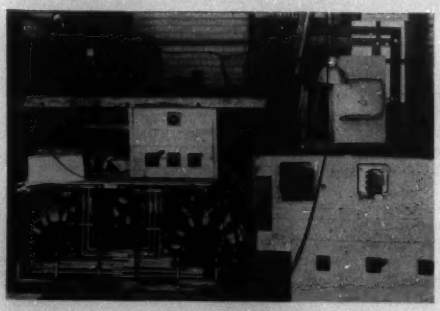

Figure 7. Arrangement of test equipment

Air gap reactor at upper left
D-c saturating switch at upper right
Current transformers with primary bar stacking at lower left
Relays under test at lower right

Table IV. Internal Fault. Voltage and Current on Relay Units with Internal-Fault Current, Using Four Thyrite Disks

Current Transformers		Calculated Rms* Symmetrical Secondary Amperes	High-Impedance Relay					Thyrite Relay Unit			Oscillogram Number See Figure 10
			Volts					Amperes			
Number Total	Rating	I_f'/N	Crest Measured Voltage	0.707 X Crest	Pick-Up Setting	Maximum Equivalent Measured	Cycles to Close	Pick-Up Setting	Crest Measured Amperes	Cycles to Close	
5	60 kv 600/5 amperes	120	1,330	940	90	230	2¼	**	43	**	79
5		110	1,310	920	160	270	7¼	**	35	**	80
5		120	1,340	950	240	320	6¼	**	54	**	81
9	15 kv 2,000/5 amperes	88	1,900	1,350	260	†	2½	8	90	1½	165
9		44	1,800	1,300	180	†	3	8	75	1¼	167
9		12	1,300	900	180	†	3	8	26	2	169
9		7.6	1,100	790	180	†	3¼	8	15	3½	171
9		3.8	930	660	94	360	2	8	†	∞	174
9		3.8	930	660	180	450	3¼	8	†	∞	173
9		3.8	930	660	310	420	6¼	8	†	∞	175

* Calculated as for Table I.

** Current relay not connected for this test.

† Not measured.

tion of the "fault CT" and "source CTs" in the test insures that for most conditions the "fault CT" will saturate completely during the first cycle of offset external-fault current, and that the "source CTs" will remain unsaturated as long as possible, thus giving both a crest value and an equivalent value which a longer time constant would not increase. A time constant even considerably shorter would not affect the crest value with residual flux appreciably, but it would tend to reduce the equivalent value by permitting more rapid recovery from the saturated condition.

Lines 4 and 5 of Table I are included to show the effect of the relative polarity of residual flux and offset external-fault current on the crest value of error voltage. These show about 60 per cent reduction in crest value, although the reduction in equivalent voltage on the resonant-circuit unit would be much less. All of the tests used as a basis for calculation of proper relay settings were made with the residual and offset additive in the fault-current transformer and subtractive in the source-current transformers.

High-Impedance versus Low-Impedance Relay

Lines 3 and 4 of Table I are included for comparison with Table II which shows the performance of the same typical bushing current transformer from the standpoint of application of a low-impedance instantaneous differential-overcurrent relay. These lines show that the effect of the error voltage on the resonant-circuit unit is only about 11 per cent of the measured crest voltage, so that the relay can safely be given a setting that is very low in comparison with the voltages shown in Table IV which covers internal faults. This large margin between external-fault voltage and internal-fault voltage is in contrast to the situation with a low-impedance relay as discussed under Operating Principles and shown in Table II.

Lines 2 and 3 of Table II in comparison with line 4 show that the relative polarity of residual and offset makes more difference in error current than does a reasonable value of lead resistance. The apparent reversal of "source-CT" secondary current between oscillograms 137 and 144 of Figure 9 is due to an intentional reversal of oscillograph leads.

Thyrite Circuit

Table III shows typical test data for calculating the proper setting of the Thyrite tap and of the Thyrite relay unit. This unit is used only to increase the speed of operation for severe faults where that is desirable; the sensitive unit will always operate, but a-c core saturation and the necessary presence of the Thyrite limiter prevent the equivalent applied voltage from reaching the high multiple of pickup that would be necessary for 1-cycle operation with the resonant circuit. The number of Thyrite disks recommended for use in service is twice the number used in test to determine the threshold of relay pick-up, so that a 2/1 margin is obtained; the tests for this purpose were made with the residual and offset additive in the "fault CT" and subtractive in the "source CTs."

Internal Faults

Figure 10 and Table IV show the wave forms and measured values of voltage and current, and the operating times, from typical internal-fault tests. Comparison with the corresponding equivalent relay voltage or the number of Thyrite disks in Tables I and III shows the margin available against false tripping on external faults or failure to trip on internal faults. Core saturation introduces a variation of nearly one cycle in the tripping time, because it may happen to prevent any appreciable output voltage on the first cycle of a fully-offset internal-fault current. However, even a relatively small negative loop of current at the end of the first cycle will saturate the core in the opposite direction because of the high impedance of the relay circuit, thus giving a half-cycle output limited mainly by the Thyrite; subsequent cycles will have substantially full output voltage on each half cycle because the core is saturated in opposite directions during alternate half cycles in spite of the presence of the d-c component. Oscillograms 79, 80, and 81 were taken with offset current and without any attempt to reduce its effect, so they show normal performance as regards the voltage obtained; the operating times are longer than normal because the Thyrite relay unit had not yet been connected when they were taken, and also because the voltage settings used for oscillograms 80 and 81 are higher than would normally be used.

Application Data

Many applications in the transmission-voltage class will be found to have adequate sensitivity on the basis of the simple RI_f'/N calculation; the only information necessary is the excitation curve corresponding to Figure 4, the secondary resistance, the resonant-circuit impedance, and the 4-disk Thyrite impedance, Figure 2. All effects of offset should be neglected in this calculation. This procedure will show successful application on some existing busses, regardless of the age or type of circuit breakers.

For more difficult applications such as the lower-voltage classes, advantage may

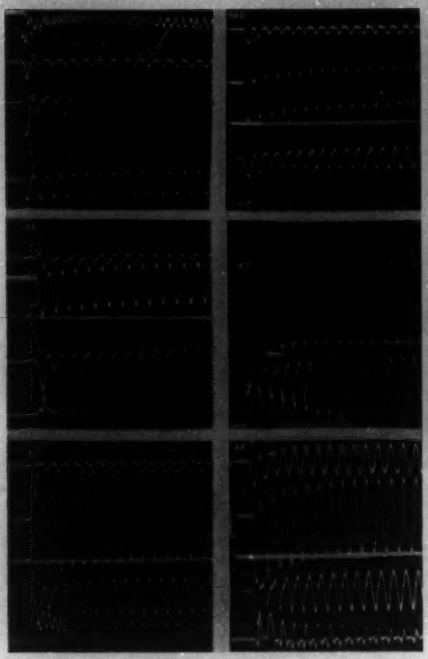

Figure 9. External-fault oscillograms

Oscillogram number 76 103 131 137 142 144
See Table number I I I II III II

ORDER OF TRACES

Number 76
High-impedance-relay d-c amperes
Error voltage
"Fault-CT" secondary amperes
Primary Amperes

Numbers 103, 131
Error voltage
Primary amperes
Contacts
"Fault-CT" secondary amperes
High-impedance-relay d-c amperes

Number 142
Error voltage
Primary amperes
Contacts
"Fault-CT" secondary amperes
Thyrite amperes

Numbers 137, 144
"Source-CT" secondary amperes
Primary amperes
Contacts
"Fault-CT" secondary amperes
Low-impedance-relay a-c amperes

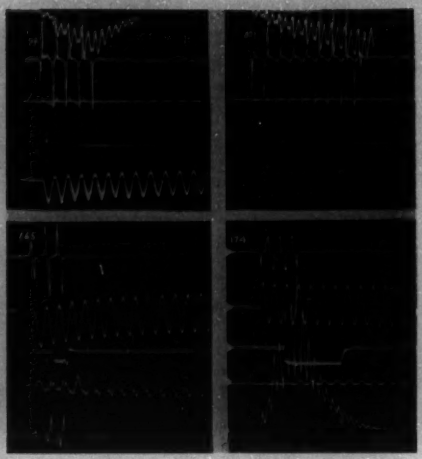

Figure 10. Internal-fault oscillograms
See Table IV

Numbers 79, 80
High-impedance-relay d-c amperes
CT output voltage
Thyrite a-c amperes
Contacts
Primary amperes

Number 165
"CT" output voltage
Primary amperes
Contacts
"Fault-CT" amperes
Thyrite a-c amperes

Number 174
"CT" output voltage
Primary amperes
Contacts
"Fault-CT" amperes
High-impedance-relay d-c amperes

be taken of the additional margins discussed under Operating Principles. This is done by calculation based on the results of factory tests on transformers having an excitation curve similar to the ones in question. These calculations, based on assumed values of lead resistance, have been made for all bushing current transformers used with the listed circuit breakers of one manufacturer. The resulting data are available in the form of Table V. Similar data for bushing current transformers having excitation curves of different shapes must be based on similar tests with those transformers.

The ratio

$$\frac{\text{maximum external-fault current}}{\text{relay pick-up current}}$$

for the five examples in the table varies from 75 to 130; the lowest ratio among the remaining 55 ratings now in production is 14.

Conclusions

In conclusion it is pertinent to summarize certain outstanding advantages of the bus differential system described in this paper.

1. Standard relaying type bushing current transformers are used, for which only the open circuit excitation data and secondary winding resistance need be known.

2. The system gives high-speed protection for moderate and severe faults.

3. Performance for specific applications is subject to simple calculation, which will show a large margin in many cases.

4. The operating conditions imposed on the current transformers by the high-impedance relay give rise to margins not available with a low-impedance relay

and these margins have been determined by test.

5. Many applications with new or old current transformers may be made successfully with careful planning, even though preliminary examination based on the RI_f'/N voltage drop may indicate that the system will not apply.

6. In limiting cases, it may be necessary to reduce the maximum secondary resistance by making the leads short and large. This can usually be done easily in the planning stage.

7. Paralleling of the current transformers should be done as near the circuit breakers as possible. Only four conductors need be brought to the relays from the junction point to provide 3-phase protection of a bus section. The leads may be paralleled in the switchyard or even at the circuit breaker terminals, with consequent saving in cost.

8. Designers may wish to use the same standard current transformers jointly for other functions; the performance of the system under these conditions can be calculated, but consideration must also be given to the hazards of false operation due to extra connections and errors in testing the added devices.

9. Protection is extended easily if the number of connections to the bus is increased.

Definitions

e_d = voltage between junction points for external fault, instantaneous value.

i_d = differential current in whole relay, instantaneous value.

i_f = "fault-CT" secondary current, instantaneous value.

i_f' = "fault-CT" primary current, instantaneous value.

$i_{(n-1)}$ = secondary current in each "source CT," instantaneous value.

$i'_{(n-1)}$ = primary current in each "source CT," instantaneous value.

I_{cp} = current in current relay and Thyrite resistor at pick-up of sensitive relay, rms value.

I_{sp} = exciting current of each "CT" core at pick-up of sensitive relay, rms value.

I_f = "fault-CT" primary current, rms symmetrical value.

$I_{sp'm/n}$ = primary current at pick-up of sensitive relay, rms value.

I_{rp} = current in resonant circuit at pick-up of sensitive relay, rms value.

H_f = magnetomotive force in "fault-CT" core.

$H_{(n-1)}$ = magnetomotive force in each "source-CT" core.

m = total number of source current transformers per phase.

n = total number of current transformers per phase.

N = secondary turns of each transformer (primary = 1).

R = resistance of longest "CT" branch between junction points, secondary winding plus leads.

R_d = resistance of differential relay.

ϕ_f = flux in "fault-CT" core.

$\phi_{(n-1)}$ = flux in each "source-CT" core.

T = time constant L/R of primary circuit.

Appendix

Open-Circuit Voltage Across Junctions with External Fault

It is assumed that all "CT" branch resistances are alike, and that all the "source-CT" currents are equal; these assumptions permit conversion from the actual circuit of Figure 11(a) to the equivalent circuit of Figure 11(b).

Noting that the induced voltage in the "source-CT" branch of Figure 11(b) is the same as that in each of the $(n-1)$ "source-CT" branches of Figure 11(a), the current-transformer loop of Figure 11(b) gives

$$10^{-8}N\frac{\phi_f}{dt}-Ri_f+10^{-8}N\frac{d\phi_{(n-1)}}{dt}-\frac{R}{n-1}i_{(n-1)}=0 \qquad (1)$$

For this condition the driving voltage in this loop is divided between the "fault CT" and the $(n-1)$ "source CTs," and

$$e_d=10^{-8}N\frac{d\phi_{(n-1)}}{dt}-Ri_{(n-1)}$$
$$=-10^{-8}N\frac{d\phi_f}{dt}+Ri_f \qquad (2)$$

If it be assumed that core saturation reduces $d\phi_f/dt$ toward zero regardless of H_f, the term $-10^{-8}N\frac{d\phi_f}{dt}$ decreases, and e_d approaches the limit

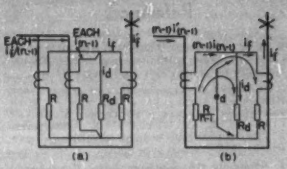

Figure 11. Circuit for analysis of external-fault conditions

a—actual
b—converted

$$e_d=10^{-8}N\frac{d\phi_{(n-1)}}{dt}-Ri_{(n-1)}=Ri_f \qquad (3)$$

If it be assumed further that the "source CTs" perform ideally and are not subject to core saturation

$$H_{(n-1)}=0=i'_{(n-1)}-Ni_{(n-1)} \qquad (4)$$

and

$$i_{(n-1)}=i'_{(n-1)}/N$$

Now $i_f'=(n-1)\,i'_{(n-1)}$, and for open-circuit conditions $i_d=0$, so

$$i_f=(n-1)i_{(n-1)}=(n-1)i'_{(n-1)}/N=i_f'/N \qquad (5)$$

Substituting in equation 3

$$e_d=Ri_f'/N \qquad (6)$$

If $i_f'=I_m[\cos\ \alpha E^{-\theta/T}-\cos\ (\theta-\alpha)]$ and $\alpha=0$ (fully offset wave) the maximum value of i_f' is $2\ I_m$. Since for a sine wave $I_{mf}'=\sqrt{2}I_f'$, the maximum value of i_f' is $2\sqrt{2}I_f'$

Then

$$e_{d\max}=2\sqrt{2}RI_f'/N$$

Change in Voltage Across Junctions, Due to Presence of Relay

If $R_d<\infty$ and the same assumptions are made,

$$i_f+i_d=(n-1)i_{(n-1)}$$
$$=(n-1)i'_{(n-1)}/N$$
$$=i_f'/N$$

$$i_f=(i_f'/N)-i_d$$

$$e_d=Ri_f'/N-Ri_d$$

which is smaller than before.

Table V. Application Data for Typical Bushing Current Transformers

Rating Type	Rating MVA	Rating Kilovolts	Rating Amperes	Kilo-amperes at which Settings Apply	Current Transformers: Secondary Turns	Current Transformers: D-c resistance at 95C to Housing Terminals	Number of Circuits	Resistance: 2-Way Lead Resistance Housing to Junction*	Resistance: Total Resistance for Field Check	Relay Settings: Volts	Relay Settings: Amperes	Relay Settings: Disks	Expected Pick-Up of Sensitive Unit: Secondary Amperes	Expected Pick-Up of Sensitive Unit: Primary Amperes
439-3500	230		800	10.0	240	1.450	6	0.35	1.80	140	4	3	0.67	160
439-3500	138		1,200	15.5	120	0.758	7	0.312	1.07	145	4	4	1.29	150
339-1500	69		600	20.0	120	0.707	8	0.203	0.91	125	11	4	1.19	140
339-1000	34.5		2,000	30.0	400	1.445	10	0.195	1.64	155	16	2	0.99	400
ARA-1000	15		3,000	60.0	600	2.10	14	0.100	2.20	265	22	4	0.76	460

* Based on standard minimum bay spacings for high voltages, or unit spacings for 15 kv, using number 19/22 conductor with junction at middle of bus run.

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Discussion

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): The authors of this paper are entitled to a great deal of commendation because they have approached the problem of high-speed bus differential protection from a new viewpoint and have found a solution which avoids the disadvantages encountered with special types of current transforming devices or the hazards of complex determination of saturation effects. The difficulties encountered in the past with high-speed bus differential relaying have been recognized for a long time and the generally accepted theory is well paraphrased by the remark, "Bus-differential relaying is limited by the current transformer performance." Many attempts have been made to provide a simple method of evaluating the current transformer performance. Other attempts have been made to overcome known or suspected saturation effects. Still other solutions were offered using special designs of transformers to obtain nonsaturating performance.

The authors now show that the previously accepted opinion that conventional designs of current transformers were totally inadequate for the bus protection was in error. By simply changing the relay sensitivity from amperes to milliamperes, that is, replacing a current coil by a voltage coil, it is shown that good sensitivity can be obtained for internal faults with adequate safety against incorrect operation on external faults.

The arrangement used has an apparent paradox in that operation for internal faults means essentially open circuit operation of the current transformers and hence requires the voltage controlling action provided by the Thyrite resistors. On the other hand, normal operation and operation during external faults will be the usual, closed-circuit, circulating-secondary, operation with which we are all familiar.

Unquestionably, there have been cases where the application of bus differential protection has been postponed because none of the previous solutions seemed attractive. Now, with a bus differential relay system available for operation from standard bushing-type current transformers, there is no reason why bus sections should not be provided with high-speed protection comparable to that provided for line sections.

W. A. Lewis (Illinois Institute of Technology, Chicago, Ill.): The essential feature of the bus-differential protection method described by the authors of this paper is the use of a nonlinear relay characteristic which is sensitive under minimum fault conditions but is insensitive for external faults. This basic concept is not new, and has been used in one form or another in various types of relays in the past. The novel features of this paper are the means which have been used to accomplish this result. The features are illustrated in Figure 5 of this paper. The essential elements are as follows:

1. The use of a resonant circuit.
2. The use of capacitors to obtain desired circuit characteristics.
3. The use of dry-type rectifiers.
4. The use of a Thyrite resistor element.

The changes which have taken place in the relaying art in the last 15 years may be summarized by stating that probably no relay engineer of 15 years ago would have considered the use of any one of these four elements in a current protective circuit. Capacitors were considered unreliable, and resonant circuits as dangerous in a current or protective circuit. The use of dry-type rectifiers and Thyrite in such circuits would have been rejected as dangerous. The possibility of failure and the uncertainty of the characteristics would have been the principal reasons for rejection. Yet the authors of this paper have presented a method of protection which is essentially dependent upon the use of these devices for its operation, and they have felt it unnecessary to offer any apology for the use of these devices or to justify them individually. The general acceptance of devices of this kind has gradually come about during the past 15 years without any specific recognition of that fact.

In the appendix, equations are given for the open-circuit voltage across the relay junctions with an external fault present. In these equations, voltage and current are given in meter-kilogram-second units, but magnetic flux is given in centimeter-gram-second units. This results in the presence in the equations of the factor 10^{-8}. Such factors are a nuisance, and can be avoided by the complete use of the meter-kilogram-second system. As official sanction has now been given to the use of this system, I would like to urge the authors of this paper and other relay engineers to simplify their analyses by the universal use of this system. Only very little study is required to become familiar with the meter-kilogram-second units of flux and other quantities which are different from those in common use in equations in practical units.

A casual review of Figure 2 of the paper does not show clearly the very large change in resistance of the Thyrite resistor which occurs as the impressed voltage changes. The curve looks nearly like a straight line, which would correspond to a resistor which did not change in value. The very large range of resistance utilized in this case can be visualized if the resistance is computed. From the figure, the resistance is found to be about 6,000 ohms at 120 volts, but less than 300 ohms at 400 volts. This is a resistance change of approximately 20-to-1, as the voltage changes less than 4-to-1.

A qualitative basis for determining the margin between trip and nontrip conditions may be obtained by referring to Figure 1 of the paper. In that figure, an external fault is shown at E. For such a condition, current is fed into the bus from the generator and from circuits L and S, but all current must be fed out to the fault at E. If the current transformer at F saturates completely, so that there is no voltage output or current output from the secondary of this transformer, the apparent exciting impedance, as viewed from the secondary terminals, is zero, and the only impedance is the secondary winding resistance and leakage reactance, if any. For bushing current transformers, the authors have assumed that the leakage reactance is zero, and so have taken the impedance as the secondary resistance. If the fault is moved from E to the bus side of the circuit breaker on the same feeder, but with no other system changes, and the circuit breaker on this feeder is opened, the primary currents in all the source current transformers will be the same as for the fault at E. In this second case, however, the

fault is an internal one for which the relay should trip. Now the apparent impedance of the current transformer F is that measured from the secondary terminals with the primary circuit opened, as determined for a typical case from Figure 4, by dividing the voltage by the current. This impedance includes the secondary resistance and leakage reactance, if any, and, in addition, the true exciting impedance. The difference in impedance as seen from the secondary terminals of the current transformer F for these two conditions must be sufficient to provide selectivity for the relay.

C. E. Asbury (The Commonwealth and Southern Corporation, Jackson, Mich.): It is encouraging to see further development work being done on bus protection. There is a definite need in the industry for a reliable bus protective arrangement to provide phase and ground protection having reasonably good sensitivity. Such a bus protective scheme should make use of standard bushing current transformers available on existing circuit breakers as the one outlined in this paper. It is hoped that such a protective arrangement can be kept simple.

It appears that rather high voltages will be encountered on the current transformer secondaries where this bus protective scheme is used perhaps as high as 1,000 to 1,500 volts may not be uncommon for heavy short circuits. This high voltage will appear across the secondary of the "CTs" as well as across the relay at the switchboard, stressing the insulation and may produce breakdown in existing current transformer insulation where the "CTs" are quite old. It would be well if the authors would state whether any special insulation is required on an installation of this scheme. The relay committee is considering a test voltage of 1,500 volts for new relays, and a voltage test less than this for older installations in the field. It appears questionable whether other relays in this circuit could withstand these high voltages without causing difficulty.

The statement is made in the paper that this system should not be used on partial windings of current transformers without special consideration. Apparently this would mean using the full winding up 120/1 ratio on 600-ampere bushing current transformers and a 240/1 ratio on 1,200-ampere current transformers. Apparently this is necessary to avoid autotransformer action of the tapped multiratio transformers. It would be helpful if the authors would point out how this affects the relay sensitivity.

It would also be helpful if the authors could elaborate on the following points concerning the application of this new bus protective relay:

1. Do these relays provide for phase and ground fault protection and how many relays are required for a complete phase and ground protection?

2. Have standard adjustable ranges been established or does each application require special design?

3. Can these relays be applied on meagerly grounded systems having impedances in the system neutral? What are the limitations to such applications?

4. In equation

$$\frac{2\sqrt{2}RI_f'}{N}$$

is not the branch "CT" leads having the largest RI_f' product the one to be considered for external faults in setting the relay instead of the maximum I_f' and largest R which might not be associated with each other for any external fault conditions?

5. Since the sensitive tripping relay is a voltage relay and may be connected to two rather long leads between the switchboard and the switchyard it appears that this circuit would be subject to induced voltages due perhaps to the flow of heavy ground fault currents. Would the authors recommend that any precautions be taken against these induced voltages in an installation?

H. T. Seeley and **Frank von Roeschlaub**: Kennedy has aptly characterized the distinctive and fundamental characteristic of the new protective system in terms of changing the relay sensitivity from amperes to milliamperes, and the calibration from current to voltage.

Lewis points out the combination of relatively new elements necessary to the schemes' successful operation. Fortunately for the acceptance of the scheme, the resonant circuit, capacitors, dry-type rectifiers, and Thyrite resistors in current transformer circuits, have each been accepted separately and have accumulated a performance record. It is a tribute to protective engineers who have to operate power systems with these devices, that progress has not been impeded simply because of their newness when first introduced. As authors, we now appreciate the advantages that Lewis points out for the meter-kilogram-second system.

The basis for discrimination has been analyzed concisely by Lewis. The difference in impedance of the current transformer on the feeder in question can be relied upon after the first cycle of internal fault current during which residual core flux may decrease the magnetizing impedance below the values calculated from Figure 4. The effect of this difference in impedance can be seen by comparing the relay equivalent voltages of Table I for severe external faults with the corresponding voltages of Table IV for internal faults of less than current-transformer rating.

The insulation strength of current transformers, which concerns Asbury, is covered by paragraph 4.047 of ASA Standard C57.1, which requires new transformers to be capable of withstanding continuously either 3,500-volts crest or operation at rated current with the secondary open, whichever is lower. It is common practice to test installed apparatus at 75 per cent of the original value, which would be 2,600-volts crest in this case, and this allows about 20 per cent above the crest value that was taken as a basis for application of this scheme. The crest value in most installations would be lower. It is the authors' belief that standard current transformers, control cable, and panel wiring are adequate for this service, subject to an initial test and infrequent periodic tests of this insulation.

While the use of other relays on current transformers used for differential protection is not normal, it is within the capabilities of the scheme on some installations and therefore the question of voltage stress on other relays is proper. Relays for the common functions, adequate for a field test of 2,500 volts, should be specified where needed for use in these circuits. The differential relay is already designed for this test.

The caution against use on partial windings was based on lack of knowledge of the leakage reactance of these partial windings, but the problem of voltage also warrants consideration. Where all current transformers are of the same ratio the use of the full windings instead of taps will allow even lower voltage setting of the relay and usually a better instead of a poorer sensitivity of protection. For the remaining cases such as 1,200/5 current transformers which must be paralleled on 600/5 taps with 600/5 current transformers on the same bus section, the voltage problem can be solved by connecting fewer Thyrite disks, at some slight loss of sensitivity, which can be calculated.

In answer to Asbury's numbered questions.

1. Three relays are required for combined phase and ground protection of a section of 3-phase bus.

2. The standard adjustment range of 100–400 volts for the sensitive unit and 4–100 amperes for the Thyrite unit are calculated to cover 98 per cent of one manufacturer's modern ratings of circuit breakers in typical installations.

3. The calculated sensitivities of typical installations with these circuit breakers range for 8 to 57 per cent of current transformer primary rating, and average 19 per cent, so the sensitivity is believed adequate for the majority of impedance-grounded systems (not including Peterson coils).

4. In the equation for maximum external-fault voltage it is proper, as Asbury suggests, to associate each external-fault current with its own branch resistance and use only the largest product, and this will permit the greatest sensitivity. The expression used in the paper, associating the maximum external-fault current with the maximum branch resistance, was adopted to simplify the calculations and will in some cases indicate a higher setting than necessary, but probably the refinement would be applied only if the simpler method showed inadequate sensitivity.

5. The voltage induced by heavy short circuit currents can be neglected in comparison with the relay setting of 100–400 volts if the precaution is observed of running all four leads from the junction to the three relays through the same conduit, so that the longitudinal voltages in the two leads of any one relay are opposite with respect to the circuit of that relay and approximately equal.

Relay Preference Lockout Circuits in Telephone Switching

AMOS E. JOEL, JR.
MEMBER AIEE

IT IS a well known principle in telephone switching practice that equipment be provided in sufficient quantity to serve the calls offered during a busy hour. Under normal conditions only an occasional call should encounter a lack of facilities to handle the call. Usually during a busy hour an average of from 5 to 15 per cent of the telephone lines are in use. Therefore, switching linkages and equipment are engineered to accommodate approximately the corresponding number of simultaneous calls. It is the object of this paper to describe, with examples, some of the philosophies which are employed in the design of one of the circuit functions which disposes available facilities among telephone users who unknowingly are competing for them.

In general, telephone connections are established over a progression of switching linkages, one or more of which constitutes a switching stage. In common control switching, as calls originate or reach a switching stage, it is convenient to serve them one-at-a-time in order that the quantity of control equipment required will not be excessive. With a small quantity of control equipment greater flexibility of control may be provided at relatively little additional cost. A switching stage may be subdivided in order that several calls may be handled simultaneously within a switching office or unit. It is possible for several calls originated simultaneously to reach the first switching stage at the same time. Thereafter several calls may reach a later switching stage or subgroup simultaneously as a result of having been served by different subgroups in the preceding stage. However, within a subgroup regardless of the stage order or size, calls are handled on a one-at-a-time basis.

There are a number of "distributive" functions in switching which have to do with the serving of calls as they originate or reach various stages at which a number of switching linkages or equipment components are available for use. Electromechanical switches have been employed in performing many of these functions. In common control switching where greater speed is required in performing these functions, relay circuits are used. One of the distributive functions which is encountered in this type of switching is described in this paper. This function is known as the "preference lockout."

Relay Preference Lockout—General

The preference lockout may be defined as the switching function which upon direct request permits service to only one of a plurality of switching linkages. The term "preference" is used in the name of this function to distinguish it from "line or signal" lockout such as used in telephone switching to prevent troubles in outside plant from holding switching equipment out of service. To a preference lockout circuit, switching linkages or equipment components are usually sources of telephone traffic seeking the services of a particular circuit or function available for the common use of the group of sources. There are many types of preference lockout circuits employed in telephone switching.

These circuits contain some or all of the following design characteristics:

1. Respond to a single indication, preferably over a single lead (in a grounded return system), as a request for service from a source.
2. Serve only one request at a time.
3. Serve requests from all sources with as little inequality of delay as possible as a result of preference position.
4. Complete the serving of a source upon the withdrawal of its original request or maintain connection with the common serving circuit independent of the original request from the source.
5. Detect and indicate abandonment of a request while it is being served.
6. Contain no inherent properties which under some conditions prevent serving or being served.

These characteristics of preference lockout circuits may be understood from the following description of typical applications.

End Relay Preference Lockout Circuit

In manual switching using the straightforward method,[1] calls arrive more or less at random at "B" switchboard positions in the call completing office. To provide rapid service, some switchboards are designed to connect automatically the incoming trunks to the operator position and telephone set.

Referring to Figure 1, the incoming trunk circuit detects when the trunk is seized at the originating office and places ground on the *ST* lead to the preference lockout circuit. An operator serves calls on any one of the 48 incoming trunks which appear as single plug-ended cords at her position. Two relays, an *ST* and an *LS* relay, are provided in the lockout circuit for each trunk. The ground on lead *ST* operates a relay *ST* to battery over a chain of break contacts on the *LS* relays associated with the higher numbered trunks. With relay *ST* operated, a series path is closed through the winding of relay *LS* associated with this trunk and the winding of relay *A* to ground on the break contact of relay *B*. Relays *A* and *LS* operate in series. When relay *ST* operated it opened the operate path for

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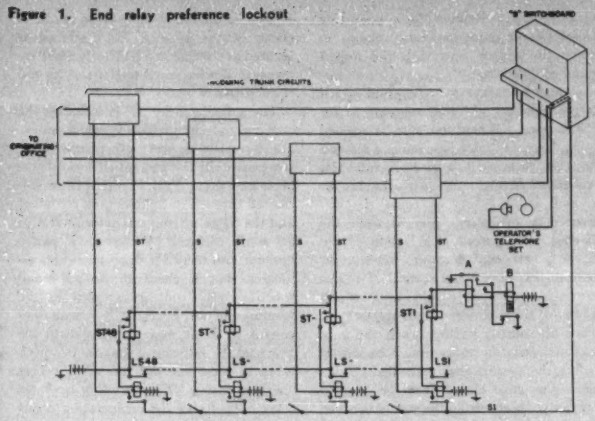
AMOS E. JOEL, JR. is with the Bell Telephone Laboratories, Inc., New York, N. Y.

Figure 1. End relay preference lockout



LS relays of any trunks to the left, preventing them from operating. Similarly when relay *LS* operates it opens the battery path which prevents the *ST* relays of any trunks to the right from operating. In this manner, all other trunks are locked out.

Thus one call would be served. The position and operator telephone set is now connected to the trunk through one or more contacts multiplied on the *LS* relays. Relay *B* operates through relay *A* after the latter has supplemented the grounds for holding itself and an *LS* relay operated.

While this call is being served by the operator, others may arrive on any of the other trunks. A most important attribute of a lockout circuit is the order in which calls are served. This order is based on three principal factors:

1. The time when service is requested relative to other requests.

2. The positional order in the lockout preference.

3. The nature of the lockout circuit action.

In the particular circuit under consideration, as calls arrive on trunks to the right of the trunk being served, no action occurs in the lockout circuit since battery for the associated *ST* relays is cut off at the break contact of the operated *LS* relay. However, calls arriving on trunks to the left of the one being served may operate their associated *ST* relays, thereby preparing them to be served.

When service has been completed for a call on a trunk connected with the position and operator, the associated *ST* relay releases. The *LS* relay associated with the next operated *ST* relay to the left will operate in series with the *A* relay. (Continuity contacts on the *ST* relays insure that relay *A* will not release when a new *LS* relay is being inserted in its holding circuit.) The *LS* relay of the trunk just served will release. Thus the chain of contacts on the *ST* relays serves to insure that only one *LS* relay operates at a time.

Since service is permitted only to trunks to the left and these trunks are gradually served, the condition will eventually occur where the release of an *ST* relay will no longer provide a path through an operated *ST* relay to hold relay *A*. Relay *A* will release immediately, opening its holding circuit which is also the operating circuit for *LS* relays. Relay *B* is also permitted to release, but being slow to release it prevents the immediate reoperation of relay *A*. During this interval when all *LS* relays are normal, the *ST* relays of all trunks requesting service are permitted to operate, particularly those to the right which were formerly locked out.

The minimum release time of relay *B* is greater than the maximum operated time of any *ST* relay to insure that any trunk requesting service will have ample time to operate its *ST* relay. When relay *B* finally releases, the process of serving may again begin. End relay *B* of this lockout circuit performs a function essential in all preference lockout circuits, namely, no switching linkage may, as a result of normal circuit operation, be perpetually locked out due to action within the lockout circuit, produced by other linkages being served.

The order in which this type of lockout circuit serves calls should be noted. If the serving of each call is completed before the arrival of the next call, then calls are served in strict time order. However, if calls arrive while a call is being served, all calls waiting on higher numbered trunks are served before any lower numbered trunks, for example, calls arrive in the following order on trunks 5, 4, 2, 6 and 1 while a call is being served on trunk 3. Subsequently, these calls are served under control of the end-relay type of lockout circuit in the order: 4, 5, 6, 1, 2. If the call on trunk 3 is finished being served before the calls arrive on trunks 4, 2, 6, and 1, then the calls are served in the order: 5, 6, 1, 2, 4.

Double Chain Preference Lockout Circuit

Throughout the various crossbar switching systems[3,4] the double chain lockout circuit is used to a considerable extent. In these systems, each of the control devices at a given stage, such as markers, must work with all the switch frames in that stage. Hence, markers compete with one another for access to these frames to obtain exclusive control of the operation of crossbar switches located thereon.

In this circuit, shown in Figure 2, only one relay is associated with each marker served by the frame. When a marker requests access to the frame, the associated lead *ST* is grounded. The associated *MP* (marker preference) relay operates provided there is no other marker being served at this time.

Battery is supplied to the *MP* relay over a chain of break contacts on the higher numbered *MP* relays. An *MP* relay in operating locks independent of this break contact chain. Continuity contacts are used between the lockout and locking paths to insure that the operate path will not open until the lock contact is closed if an *MP* relay operates after a

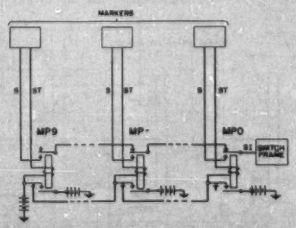


Figure 2. Double chain preference lockout

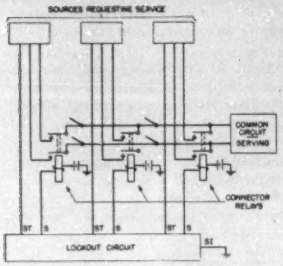


Figure 3. Use of connector relays with preference lockout

higher numbered *MP* relay is operated. The order of serving of this type of lockout circuit is the same as that of the end-relay type of lockout circuit.

Chains of transfer preference contacts (only one shown) may connect the switch frame points with the marker being served. Usually a great many leads must be connected through between a marker and a switch frame. Figure 3 shows how a single transfer preference chain on the lockout circuit may be used to control a connector circuit to obtain a larger number of paths between the marker and the switch frame.

Gate Preference Lockout Circuits

Gate lockout circuits are used in crossbar systems where it is desirable to serve equitably calls in the order of arrival. This is particularly true in the controllers reached first by calls entering the switching equipment (lines or incoming trunks). The action of this type of circuit permits all calls waiting at the time the gate closes to be served before any calls which may arrive later.

Figure 4 shows a gate lockout circuit. The preference relays *H* are operated on requests for service when the gate is open (relay *GT* normal). Once an *H* relay operates, it locks independent of the gate relay *GT* and then starts the operation of relay *GT*. Relay *GT* in operating closes one or more preference chains to serve the source operating the lowest numbered *H* relay.

Connector relays may be employed if larger numbers of leads are required for the service. When the gate is opened several sources may be awaiting service. In this case each source operates its associated *H* relay. The *GT* relay is slow enough in operating to cover the difference in operating time between the slowest and fastest *H* relay to insure against perpetual lockout when competition exists. When several *H* relays are operated the gate is held closed as long as any of these *H* relays remain operated. Thus all sources waiting when the gate was momentarily opened must be served before the sources that requested service since the gate closed may be served. Service to those within the gate is in order starting from the lowest numbered operated *H* relay. Again assuming calls requesting service from sources in the order 5, 4, 2, 6 and 1 while 3 is being served, they will be served in the order 1, 2, 4, 5, and 6 since all are permitted into the gate when it opens at the end of the service for source 3. If the service of source 3 is completed before the requests are made by sources 4, 2, 5, and 1 then they are served in the order 5, 1, 2, 4, and 6.

Gate Check Preference Lockout Circuits

The gate type lockout circuit introduces a new element in lockout circuits, namely the gate relay. An individual gate contact is used by a source on each request for service. Should this contact become open due to a trouble condition (dirt or maladjustment) the associated source will be perpetually locked out. Where checks may not be made in another manner to insure against this condition, a gate-check type of lockout circuit, described in the following may be used. A check of the type to be described is secured at the cost of a relatively small increase in time required to give service and the addition of relays.

The splitting relay *SP* is added to the gate lockout circuit (See Figure 5) to enable a request for service from any source to operate all the individual lockout relays, here designated *V*. This is accomplished through its splitting relay contact and the *SP* relay contacts associated with all other sources. When all *V* relays operate, end relay *VT* operates. This indicates that a check of all gate and splitting relay contacts as well as the windings of all *V* relays has been successful. Circuit means, not shown, are provided to indicate a failure to check when at least one but not all *V* relays operate. Relay *VT* in operating, operates relay *SP*. Relay *SP* in operating opens the paths which operated *V* relays associated with sources not requesting service. With some of the *V* relays now releasing the original operate path for relay *VT* is opened. However, before operating relay *SP*, relay *VT* prepared a path to hold in series with relay *VS* which now operates. Relay *GT* is now operated closing the gate to prevent the operating of any *V* relays by sources other than those which request service at this time. The order of service of gate type lockout circuits is the same with or without the check feature.

Single-Service Gate Preference Lockout

In the panel dial system, line finder switches serve ten groups of 40 subscribers lines each.[5] A control circuit, known as the trip circuit since it controls tripping of the line finder brushes in finding this group, originates a request for service from any of the lines in a group. The line finder action is controlled by a start circuit which is common to 10-trip

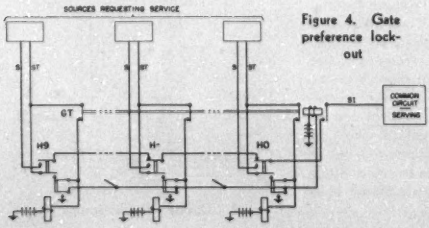


Figure 4. Gate preference lockout

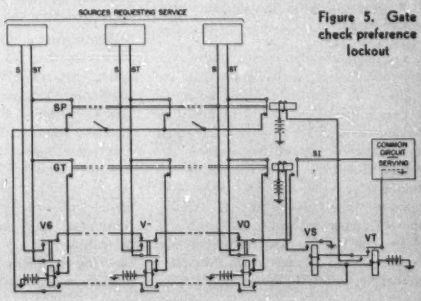


Figure 5. Gate check preference lockout

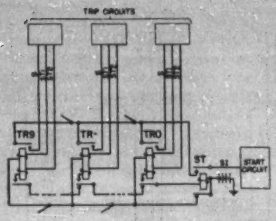


Figure 6. Single-service gate preference lockout

circuits. Line finders are started for only one line group at a time. The associated trip circuit is chosen on a lockout basis among all others requesting service at that time.

The circuit shown in Figure 6 illustrates the lockout circuit which has been used in this application. It is, in effect, a gate type lockout wherein only a single circuit, the preferred one, is admitted behind the gate when being served. Each source is represented in the lockout circuit by a double wound relay *TR*. Two leads *ST*1 and *ST*2 are closed to ground by a source when requesting service. When a source is served, relay *TR* operates by its upper winding through relay *ST* normal. When relay *TR* closes its front contact in the preference chain, relay *ST* operates in series with the lower holding winding of relay *TR* with ground from lead *ST*2. Once relay *ST* operates no other *TR* relays may operate until the operated *TR* relay is released, releasing simultaneously relay *ST*. In this circuit relay *ST* is acting as the gate relay while the preference chain and lower windings of relays *TR* permit only one source request to remain inside the gate.

The circuit as shown favors distinctly the serving of lower numbered circuits. It would thus be possible for two very busy lower numbered circuits to prevent satisfactory service from being given to higher numbered circuits. Therefore it has been the usual practice when employing single-service gate preference lockouts, to provide a form of signal lockout for each request source under the common control of all sources which are requesting service. This prevents further requests from any served source from reaching the preference until all waiting sources have been served at least once.

Preference Changes in Lockout Circuits

Aside from caring for perpetual lockout due to inherent circuit action, lockout circuits are frequently provided with associated circuit arrangements which change the preference order after each usage. Such changes prevent perpetual lockout due to circuit trouble conditions, such as open contacts in the preference chain or troubles in the individual sources being served, which prevent the removal of their request for service. Since a request is for definite services, the lockout circuit may be informed when these services have been rendered so that if the request fails to be released action may be taken to free the lockout circuit from that source. This is sometimes aided by changing the preference after each service. The changing of preference also insures that regardless of position in a lockout chain, waiting requests on the average are served equitably.

There are two methods which have been employed rather extensively to change the preference order in lockout circuits. One of these methods is to reverse the direction of lockout action and service preference on alternate usage of the lockout circuit. Such a scheme applied to the gate-check type of preference lockout circuit is shown in Figure 7. The *W* and *Z* relays comprise a recycling 2-step counter which receives a ground pulse on each operation of the *VT* relay. Relay *W* operates or releases on each ground pulse while relay *Z* operates or releases on the removal of each ground pulse. The ground holding relays *VT*, *VS*, and *SP* are released when each request has been served. Two sets of contact chains wired in opposite directions are provided on the *V* relays. One set is used when the *Z* relay is released, the other when the *Z* relay is operated.

The second method of changing preference is more generic. Here a recycling multistep counter circuit progresses the start of preference to a different point on each service request. The order of progression need not be the same as the preference chain order. Figure 8 shows this type of arrangement where for the sake of simplicity the details of sequence or counter circuit have been omitted.

With some types of lockout there is the disadvantage that, once a trouble condition develops, requests may be served at only half their former rate. By the addition of a signal or line type lockout circuit to each path which provides access to the preference lockout this condition may be eliminated.

Another way in which preference and other chains are guarded to prevent perpetual lockout is to provide two identical lockout circuits, one for regular use and the other for emergencies. When a trouble is detected provision is made for automatically transferring from one lockout circuit to another. This arrangement not only gives regular service in event of trouble but prevents perpetual lockout to

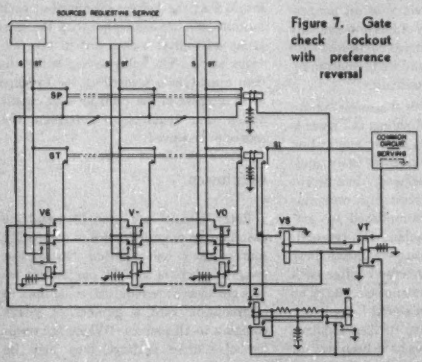


Figure 7. Gate check lockout with preference reversal

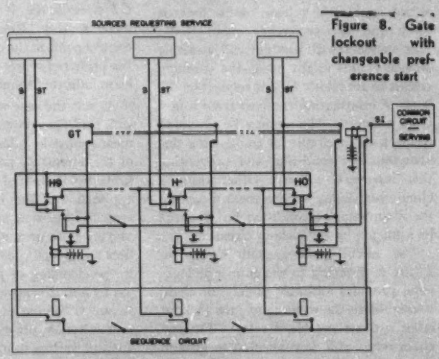


Figure 8. Gate lockout with changeable preference start

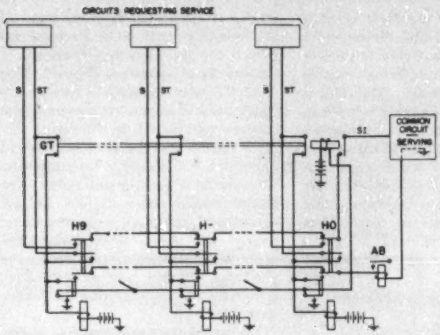


Figure 9. Gate preference lockout arranged to detect abandoned attempts

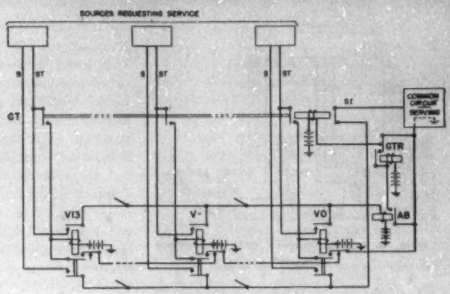


Figure 10. Improved single-service gate preference lockout arranged to detect abandoned attempts

a given source due to an open lockout relay winding. An arrangement of this type has been described in detail.[6]

Abandonment of Service Request

In some applications of lockout circuits in common control switching systems, the serving of a request is the start of a comparatively long and complex series of circuit actions by some control circuit. Furthermore the request to the lockout circuit may be the only direct indication of the service request received by the common circuit. A frequent occurrence in telephone switching is the abandonment of calls. It is desirable and sometimes necessary that the lockout circuit be arranged to detect the abandonment of the request without starting the service of a new call before the common circuit has fully restored to normal. In the circuit arrangements thus far discussed abandonment of the call being served would enable the release of the lockout relay and the preference chain would then connect with another call awaiting service. This might cause the common circuit to act falsely for one request on the basis of information received from a previous request. Means may be provided in the lockout circuit for recognizing this abandonment condition and preventing this incorrect action. Otherwise the circuit requesting service must recognize the abandonment condition but prevent its influence on the lockout circuit.

The circuit arrangement shown in Figure 9, applying to the gate-type lockouts, provides an extra preference chain connected to the winding of each lockout relay with an end chain relay. The end chain relay, *AB*, is connected to ground supplied by the common circuit giving service. Relay *AB* is normally not operated. However should a request for service be withdrawn during service, relay *AB* would operate in series with the lockout relay being served to prevent switching preference and to give an indication of the abandonment.

A more recent version of the single-service gate preference circuit with provision for detecting abandonment is shown in Figure 10. In this circuit only one *ST* lead is required per source and a gate relay with a make contact per source is used (Figure 6). This gate relay *GT* is normally operated from ground through relay *AB* normal when the source is ready to serve. When a source requests service, its associated *V* relay operates. The preferred *V* relay, if more than one operates, locks to the ground supplied by the common circuit while relay *AB* operates from the source *ST* lead ground. Relay *AB* in operating operates relay *GTR* which locks to the common control ground operating relay *GT*. Relay *GT* releases after relay *GTR* locks operated. The release of relay *GT* releases the *V* relays of all nonpreferred sources. Relay *AB* is now the only control point in the circuit connected with the preferred source and its premature release indicates abandonment.

As was the case with the single service gate preference circuit shown in Figure 6, means must be added in the application of this circuit to provide for more equitable distribution of service. In a recent application of this circuit in a cross bar system, this was accomplished by employing alone or in combination, two artifices mentioned previously. One of these is the changing of preference after each use by a multistep sequence circuit. The second is the use of a signal lockout per source which prevents further requests until all waiting sources have been served.

Tandem Operation of Lockout Circuits

In the application of the end relay lockout of Figure 1, 48 or more pairs of lockout relays, one for each trunk served by the same operator, were required. By grouping the trunks and providing one or more lockout circuits to determine the group or subgroups to be served and one or more lockout circuits to determine the particular trunk to be served within the group or subgroups the total number of lockout relays required could be reduced. An example of this tandem use of lockout circuits may be found in the crossbar system where as many as 700 lines may be served by one controller.[7] The lines are divided into ten groups of 70 lines each and each group of 70 lines is divided into seven subgroups of ten lines each. With three lockout circuits of 10, 7, and 10 inputs operating in tandem, a process of elimination takes place which permits the lockout serving of any of 700 inputs with 27 lockout relays. Care must be taken to insure that the sources examined at each lockout step are associated with the subgroup of sources locked out in the previous step. The relay saving with tandem operation is secured at the expense of an increase in the total lockout circuit action time required to determine the source to be served.

Conclusion

Relay circuits are used in telephone switching to provide service to competing sources on a one-at-a-time basis. For small numbers of sources, one or two relays per source are required, sometimes in combination with a number of relays common to all sources. Where the number of sources is large they may be

grouped to enable serving by several lockout circuits in combination. Means are sometimes added to the lockout circuit to detect troubles which prevent the serving of some or all sources. Relay type lockouts do not necessarily serve requests in the order of their arrival particularly under heavy load conditions. As a result some sources may be assigned positions where, under given load conditions, they encounter more or less than the average delay. To enable the equitable distribution of delay among all sources under all load conditions, and to mitigate various troubles which may prevent the rendering of service, preference changing means may be provided. Arrangements to detect the abandonment of calls and under such conditions to prevent the premature serving of another request may be required for some applications, particularly when directly rendering service for subscriber calls.

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Discussion

Frederick L. Kahn (Automatic Electric Company, Chicago, Ill.): The circuits described in this paper are very simple, but their function in modern high-speed switching systems is most important as they assure the orderly dispatch of calls made in rapid succession by different subscribers who compete for immediate service by equipment units or operators.

Joel is to be complimented on his lucid presentation of this problem and the methods available for control. This will assist all of us in the telephone industry to speed up and improve the process of training young engineers; it is impossible to include such material in the present college curricula.

Many organizations working in the telephone switching field have been faced with the problem of preference lockout circuits in automatic switching system. The automatic (cordless) toll board is one of the situations where an orderly handling of waiting calls is important. Circuits such as described in this paper are being used for this purpose, but in some instances more rigid sequence control has been demanded by traffic operating engineers. For such cases, there have been designed lockout and sequence answering circuits that assure the answering of waiting calls strictly in the order of arrival; that is, first come, first served.

Such circuits are more elaborate and more expensive than the preference lockout circuits shown in this paper. The need for complete sequence control is controversial. As no circuits of this type are included in Joel's paper, I should like to ask whether preference lockout circuits with "sequence of arrival" control are used by the Bell Telephone System; if so, for what type of application?

A. E. Joel, Jr.: In the Bell Telephone System arrangements are available for serving calls to information desks and order turrets in the strict order of arrival. Service of this nature is not provided by preference lockout circuits. Multifunctional circuits consisting of allotting and sequence functions are employed.[1]

Reference

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Power Distribution and Utilization in a Large Iron Ore Beneficiation Plant

A. F. GETTELMAN
NONMEMBER AIEE

Synopsis: Large iron ore beneficiation plants introduce new problems of distribution and utilization of electric power in the iron mining industry. The selection of voltage levels for generation and main distribution, large and small motors, is based on a study of installed cost system reliability and efficiency. A modern load center system of distribution of a-c and d-c power for a large plant unit is demonstrated.

RESERVES of high grade iron ore in the United States are running out. Some experts estimate that direct shipping ores of 50–65 per cent iron content will last only another eight to ten years. Of course, some ore requirements have been and will continue to be supplied from intermediate ore concentrated in plants using a simple sedimentation process of washing, classifying, and crushing when necessary. However, the low grade taconite ores of the Mesabi range must be concentrated or beneficiated in increasing amounts if we are to maintain our steel production from domestic sources.

Taconite ore occurs in both magnetic and nonmagnetic forms. The taconite to be beneficiated in the plants now being planned is a hard dense rock averaging about 24 per cent iron in the form of magnetite. The iron content is so finely divided that nearly all particles are smaller than 20 mesh. The rock must be crushed in three or four stages and then be ground to a powder so fine that 85 per cent of it is minus 320 mesh. Magnetic separators reduce the burden of grinding in the ball and rod mills and in combination with classifiers and a plentiful supply of water produce a concentrate in the form of a fine black powder. This powder must be sintered or made into hard pellets to be suitable for the blast furnaces. Nearly three tons of iron bearing rock is reduced to one ton concentrate of approximately 64.5 per cent iron.

To utilize the low grade ore, huge beneficiation plants must be built—plants that will require 25,000–30,000 installed horsepower to produce 2,500,000 tons of concentrate annually. The purpose of this paper is to discuss the problems of electric power distribution and utilization in such plants as these.

The importance of the subject can be appreciated when it is considered that 60–75 kilowatt-hours will be required to produce one ton of concentrate from taconite ore. In 1935 the Minnesota ranges produced a ton of iron ore with 3.5 kilowatt-hours of electric power. To produce 2,500,000 tons of concentrate may require as many kilowatt hours as was used to produce 63,000,000 tons of ore in 1945.

While experimental plants of about 200,000 tons annual capacity are being built, the ultimate plants for economical operation must be much larger units. Plans are now under consideration for a beneficiation plant unit with a capacity of 2,500,000 tons of concentrate annually, with ultimate expansion to four units producing 10,000,000 tons. The flow sheet of such a plant unit is shown in Figure 1, and the power requirements are listed in Table I. The approximate connected horsepower for such a plant unit is

Crushing plant—3,500 horsepower
Concentrating plant—18,000 horsepower
Sintering plant—8,000 horsepower

Power for beneficiation plant units with 30,000 connected horsepower is not available at present in the areas of the Mesabi range containing the ore reserves. Generating plants will have to be built near the plant site where conditions are favorable, and the following is based on the typical beneficiation plant with power generation at the plant site. In addition, it also is assumed that the single unit plant will be built and operated as a single unit for some period of time.

Selection of System Voltage Levels

The Table II shows the utilization equipment voltage ratings, generator, and stepdown transformer voltage ratings, and the corresponding nominal system voltages with which we are concerned in a beneficiation plant such as this.

The selection of the proper system voltage for the first plant unit and for future plant units should be on the basis of overall economy. The cost of component parts of the power system including switching equipment, distribution channels, transformers, and even the rating of motors must be taken into consideration.

The chart in Figure 2 shows the selection of the main generation and distribution level for new plants based on good engineering practice. For a plant with 30,000 horsepower installed and generation at the plant site, 13,800 volts is the preferred level for the main generation and distribution.

Selection of Motor Voltages

The horsepower ratings of motors in a modern beneficiation plant range from 1 horsepower induction motors on feeders and separators to large synchronous motors on rod and ball mills, and also on fans for a sintering plant. Plans available today on these plants do not indicate the use of motors over 1,000 horsepower.

Motors with 440-volt rating have been standard in the mining industry for the lower horsepower range, and will be used in the beneficiation plants. Many studies

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A. F. GETTELMAN is with the General Electric Company, Minneapolis, Minn.

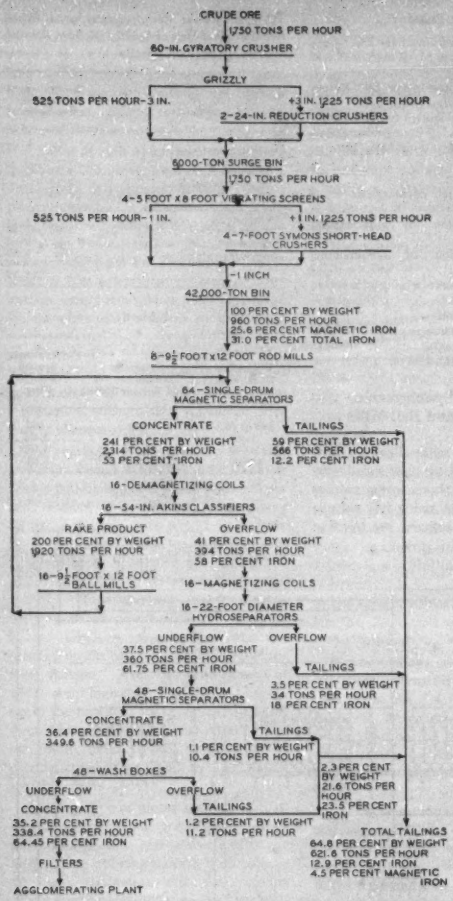


Figure 1. Flow sheet for a typical large beneficiation plant unit

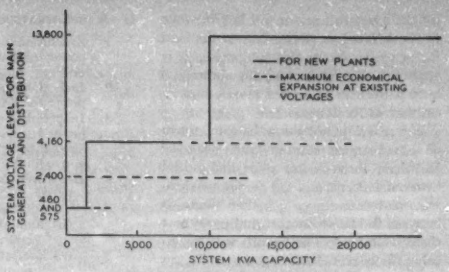
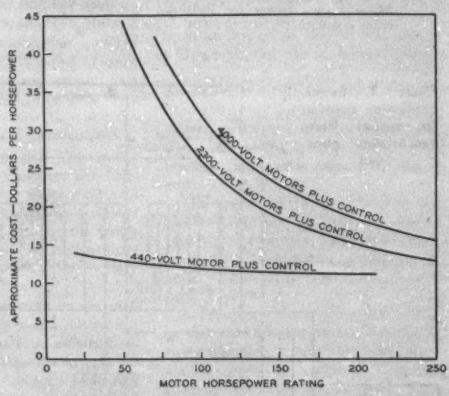


Figure 2 (upper right). Selection of main generation and distribution voltage in terms of mill electrical capacity

Figure 3 (lower right). Cost of motors and control versus horsepower rating of motors



have shown that a 220–230 volt rating would not be economical due to the increased cost of feeders, control, and switching equipment. Motors of 550-volt rating have been used in other industries such as some paper or textile mills, but new plants are using 440-volt motors due to their greater availability.

In the higher horsepower range up to 1,000 horsepower, 2,300-volt or 4,000-volt motors will be used. To determine what part of the horsepower range will use 440-volt motors, the over-all economy must be considered, taking into account the costs of motors and their controls and transformer capacity, since all motors will require installed transformer capacity with a main distribution voltage of 13,800 volts.

Transformer costs in dollars per kilowatt-ampere decrease as the transformer rating increases. However, as the transformer rating increases the fault duty in the low voltage bus increases, requiring larger and more costly circuit breakers.

For the 440-volt motors, the optimum size of unit substations ranges from 500 to 1,500 kva. Where load concentrations are high, as in the concentration plant, studies indicate the use of 1,000-kva units.

For the 2,300-volt or 4,000-volt motors in the higher horsepower range, larger transformer units are warranted, since short-circuit interrupting levels up to 150,000 kva at 2,400 volts, and 250,000 kva at 4,160 volts may be used with good economy. For the 2,300- or 4,000-volt motors, unit substation transformers of 5,000 or 7,500 kva respectively will provide the best over-all economy, taking into account, location of load, circuit arrangement, and so forth. In the circuit arrangement as shown in Figure 4, the use of 6,000-kva transformer units is indicated.

To compare the costs of motors and controls, the curves in Figure 3 have been drawn, based on standard 1,200-rpm induction motors and full voltage starting. At 200 horsepower a comparison of the 440-volt and the 2,300-volt motor and control shows an increased cost of approximately four dollars per horsepower at 2,300 volts. However, the investment cost in transformer substation capacity

for the 2,300-volt motor will be from four to six dollars less, depending on the load factor used. From this comparison it is apparent that motors up to about 200 horsepower can be served more economically at 440 volts.

Figure 3 does not show the comparison of synchronous motors and controls. However, there is less price differential between 440-volt and 2,300-volt synchronous motors and control than there is between induction motors and control at these voltages. The result will be to bring the curves closer together in Figure 3 and lower the maximum horsepower rating to be used at 440 volts.

Based on this division of motor voltage ratings, the power requirements for the plant are divided approximately as follows:

A—Crushing Plant

1. Induction motors over 200 horsepower for driving crushers and large conveyor belts.
2. Induction motors under 200 horsepower for driving conveyor belts, feeders, screens, and dust collectors.
3. Miscellaneous power for lighting, crane repair shops, and so forth.

B—Concentrating Plant

1. Synchronous motors over 200 horsepower for driving ball mills and rod mills.
2. Induction motors over 200 horsepower for driving pumps.
3. Induction motors under 200 horsepower for driving conveyors, classifiers, magnetic separators, pumps, and so forth.
4. D-c power for:
   (a) Excitation of magnetic separators.
   (b) Excitation of magnetizing coils.
   (c) Excitation of synchronous motors.
   (d) D-c motors driving adjustable speed feeders to the grinding mill.
5. Miscellaneous power including lighting repair shops, cranes, demagnetizing coils, and so forth.

Power Circuit for Synchronous Motors on Rod and Ball Mills

The rod and ball mills (Figure 1 flow sheet) will be driven by slow speed synchronous motors in horsepower ratings between 450 and 700, using full voltage starting, as is the practice in ore treating

Table I. Power Requirements for a Beneficiation Plant Unit of 2,500,000 Tons Annual Capacity

		Kilowatt-hour Per Ton of Sinter
Mine		2.32
Concentrator		
Crushing	4.51	
Primary grinding	7.47	
Secondary grinding and cobbing	26.35	
Magnetic concentration	3.24	
Filtering	1.19	
Water supply	5.06	47.82
Sinter plant		20.63
Miscellaneous facilities		0.80
Total		71.57

Table II

Utilization Equipment Voltage Rating	Generator Transformer No Load Voltage Rating	Nominal System Voltage Class
440 or 460	480	460
2,300	2,400	2,400
4,000	4,160	4,160
13,200	13,200 to 14,400	13,800

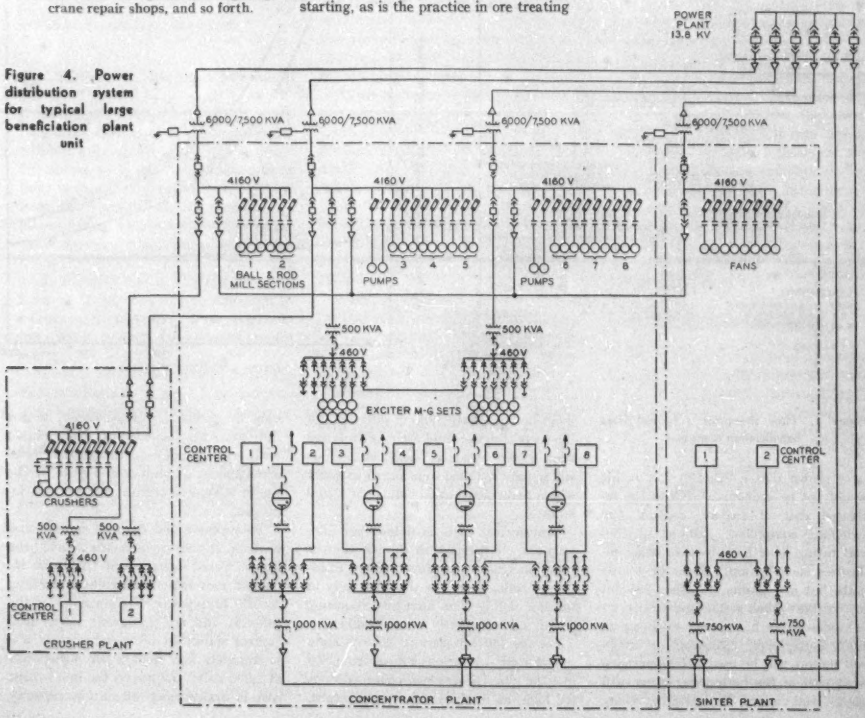


Figure 4. Power distribution system for typical large beneficiation plant unit

mills now in operation. The voltage rating of these motors can be 2,300 or 4,000. A study must be made of all factors, including cost of switching equipment, motors, controls and feeders to determine the most economical voltage rating of these motors.

A mill unit with 24 synchronous motors of 500 to 700 horsepower will require a number of small transformers and a multiplicity of switching equipment if a selective bus arrangement is used and the motor circuits are to be limited to a low interrupting level for short-circuit duty. With a low interrupting level, a large voltage drop occurs when starting a large synchronous motor. Since motor torque is proportional to the square of the voltage, it therefore is reduced considerably. The high torques required for synchronous motors used on ball and rod mills represent a substantial part of the first cost of these motors.

For example, if the interrupting level and short circuit is limited to 25,000 kva, then a 600-horsepower motor with 600 per cent locked rotor current and power factor of 40 per cent would have only $86^1/_2$ per cent voltage on starting, and this will produce only 75 per cent of the full voltage starting torque on the motor.

Full voltage starting, using electrically operated power oil circuit breakers in metalclad switchgear, has been the practice on the ball and rod mill synchronous motors. With the development of the modern high-voltage air circuit breakers of the magnie-blast type, the use of oil circuit breakers can be eliminated. The air circuit breakers will reduce contact maintenance when used in the motor starting units.

Another type of motor starting control that has found widespread use in other industries, such as the rubber and paper mills, is one utilizing modern high voltage air break contactors with short circuit protection provided by means of high interrupting capacity current-limiting fuses. These air break contactors have been used on severe operating cycles and are suited particularly for jogging operations where a mill is being rotated to a given position.

The current-limiting power fuses are co-ordinated with the motor so that they will operate only under short-circuit conditions and will not blow unnecessarily on motor starting and stalled rotor currents. The current-limiting action of the fuse reduces the fault damage by limiting the fault current.

The maximum economical ratings for power circuit breaker are shown in Table III.

High interrupting capacity motor starters with current-limiting fuses, also follow the same limits as curcuit breakers, that is, system fault duties of 150,000 kva at 2,400 volts and 250,000 kva at 4,160 volts.

From Table III it will be noted that a 600-ampere curcuit breaker is adequate to interrupt 150,000 kva at 4,160 volt, whereas a 1,200-ampere circuit breaker is required to interrupt 150,000 kva at 2,400 volts.

The power circuit breaker not only must be capable of interrupting the fault current at the time of short circuit, but also must withstand the high mechanical forces produced by the initial first half cycle of short-circuit current. There is also a limit to the current a power circuit breaker will interrupt regardless of voltage, and it also may not be able to interrupt its rated kilovolt-amperes at all voltages.

At 4,160 volts, the power circuit breakers and current-limiting fuses may be applied up to their maximum interrupting rating. A 600-ampere power circuit breaker at 4,160 volts will carry 1.73 times the kilovolt-amperes that it will at 2,400 volts. The installed cost of cable is approximately inversely proportional to the system voltage or 73 per cent more for the 2,400-volt system than for the 4,160-volt system.

Figure 4 shows a proposed power distribution system for a modern beneficiation plant using a flow sheet similar to Figure 1. A modern load center system of power distribution is used for the power requirements for both the high voltage mill motors and also the low voltage motors.

The synchronous motors on the ball and rod mills have a 4,000-volt rating. Power is supplied to these motors from master unit substations with 3-phase oil-filled transformers equipped with fans for increased supplementary rating. Each unit substation transformer has a cable junction entrance for the 13.8-kv cable from the generating plant. Oil-filled transformers are indicated as the concentrator plant construction usually will permit the transformers to be located along the outside wall of the building at locations convenient to the motor loads they serve.

Where the control room can be located along the outer wall of the concentrator plant, convenient to the transformers, a metal-enclosed bus duct is a preferred means of connecting the transformer to the metalclad switchgear. This arrangement provides a completely enclosed system from the power station to the motor. The space requirement is much less than for the older type substations using single-phase transformer banks and open bus work and disconnect switches. A high degree of safety and reliability is obtained, and switching is accomplished safely and quickly with circuit breakers of full interrupting capacity.

Normally, the substations would be operated separately. With the tie bus as shown, in the event of a transformer or a feed failure, the bus sections can be operated from the remaining three substations in parallel without exceeding the circuit breaker or fuse interrupting rating.

The 13.8/4.16-kv master unit substations consist of a transformer, metalclad switchgear which contains the tie and feeder circuit breakers, and the motor control sections using current-limiting fuse starters.

To permit using the full 250,000-kva interrupting rating of the power circuit breakers and current-limiting fuses in the motor controls when the tie circuits are closed, 4,160 volts was selected. To use 2,300-volt motors in a circuit as in Figure 4, the short-circuit interrupting level must be limited to 150,000 kva, and so will require the use of current-limiting reactor units in each of the tie circuits. A study of comparative costs shows that the savings at 2,400 volts in costs of motors and control is offset by the additional cost of the feeders, power circuit breakers, and reactor units in the switchgear.

The use of motor controls with current-limiting fuses of high interrupting capacity represents a considerably lower investment than motor controls using power circuit breakers at the high interrupting levels.

In choosing a circuit arrangement such as that shown in Figure 4, a balance between the desired reliability and the installed cost of the system must be considered. With the large capital investment in the plant and with 3-shift operation of the concentrator plant, continuous power supply is essential.

Grounding of High-Voltage Mill Circuits

The established practice has been to operate the present mills with the 2,400-volt motor circuits ungrounded; the thought being that with the system left ungrounded no loads would be lost and the system operated until some future time when the ground could be removed.

Aside from having to isolate each circuit to find the ground, there are other disadvantages of an ungrounded system.

The occurrence of a ground on one phase overstresses the windings of all machines while the ground is on. Switching surges and arcing grounds can produce serious overvoltages up to six times normal. As a result, flashovers that are difficult to explain are found at locations remote from the actual ground fault.

In Fig. 4, the neutral of the Y-connected 4,160-volt winding on each 13,800/4,160-volt substation unit is grounded through a current-limiting resistor. This resistor can be mounted in a protective metal enclosure on the transformer unit itself. Ground fault relaying is provided on all feeder circuits, and phase protection on the motor starters.

D-C Excitation of Synchronous Motors on Ball and Rod Mills

The use of separate motor-generator sets rather than direct connected or V-belt-connected exciters saves valuable space in the mill area. The motor-generator sets can be located under better operating conditions in the control room. If an individual motor-generator set is furnished for each motor, a large number of small units are required. If, on the other hand, several large motor-generator sets are used for excitation of all the synchronous motors, spare capacity and d-c distribution are important factors. The a-c motors on the motor-generator sets can be supplied best from the 4,160-volt mill motor circuits to provide the desired reliability.

In the arrangement as shown in Figure 4, a motor-generator set exciter is provided for each of the mill sections. Each mill section consists of two synchronous motors driving ball mills and one synchronous motor driving a rod mill. A spare set can be connected to any section and provides reliability with a minimum of equipment.

The exciter sets are supplied from two 4,160/480 load center units using noninflammable liquid-cooled transformers. Control for the exciter set motors may be incorporated conveniently in the secondary switching section of the load center unit. Additional 460-volt power for lighting, control battery charging and for small feeder motors on this side of the mill also can be taken from these unit substations.

Power Circuits for Low-Voltage Motors

Load center unit substations using askarel-filled transformers are available for 13.8-kv circuits and will provide the most satisfactory and economical distribution of 460-volt power. They can be located in the control room adjacent to the 440-volt motor load. With magnetic ore dust and moisture present, the use of air-cooled transformers would require special attention. Studies that have been made indicate that for the 460-volt concentrator power, units of 1,000-kva capacity will be the most economical. The use of two 13.8-kv feeders with several unit substations on each feeder and a secondary selective system is preferable to a primary selective system which would require 13.8-kv switching equipment.

Table III

Circuit Breaker Ampere Rating	Interrupting Rating, Kva
	2,400-volt service
600	100,000
1,200	150,000
	4,160-volt service
600	150,000
1,200	250,000

440-Volt Motor Control Centers

The concentration of 440-volt motors in the mill is such as to provide an excellent application of modern control centers, incorporating the short-circuit and thermal overload protection in each starter for the individual motors. With control centers located in the control room apart from the classifiers and pumps, the control equipment can be maintained more satisfactorily, with greater accessibility and with space saved in the mill.

Magnetic Separators

From the flow sheet (Figure 1) it will be noted that 64 single drum "roughing" magnetic separators are used after the rod mills and 48 single drum "finishing" magnetic separators are used after the hydro separators. Typical power requirements for the magnets in these separators are shown in Table IV.

Table IV

	Pick Up Pair Numbers 1 and 2	Tailings Pair Numbers 3 and 4	Concentrate Pair Numbers 5 and 6
48-inch drum rougher separator	3.3 kw max	4.0 kw max. Operate at 2.8 kw	4.0 kw max Operate at 2.8 kw
48-inch drum finisher separator	3.3 kw max	4.0 kw max. Operate at 3.1 kw	4.0 kw max Operate at 3.1 kw

Based on the requirements given in Talbe IV, 64 roughing and 48 finishing magnetic separators would require approximately 1,000 kw of d-c power for magnetic excitation. While the use of 125 volts direct current may be preferred in the winding of the magnetic separator coils, 230 volt d-c separators are standard and there is comparatively little difference in price. Therefore, a 250-volt d-c supply system is recommended as it will provide the lowest cost distribution system including cables, switches, and power supply equipment.

If the magnetic separators can be wound for the approximate field strength required in actual operation, then very little power loss occurs in the rheostats on the tailings and concentrate magnets where adjustment of operating strength is required. On this basis, the most economical power supply is a 250-volt d-c rectifier of the sealed tube ignitron type.

Where a wide range of magnet strength may be required and the operating losses in the rheostats of the magnetic separator are considerable, the use of multiunit motor-generator sets with separate excitation should be considered for over-all economy. The magnetic separators in any one of the eight mill sections would be adjusted as a unit, and the same voltage impressed on all tailings or concentrate coils in a roughing or finishing section from an adjustable-voltage generator.

The driving motors for the motor-generator sets will be of sufficient size so that they can be operated directly from the mill circuits which supply the synchronous motors on the ball and rod mills. Since there will be available with the high torque synchronous motors on the rod and ball mills an excess of leading reactive kilowatt-amperes, synchronous motors are not essential on these motor-generator sets.

The sealed tube ignitron rectifier will consist of a transformer section, a rectifier section, and a d-c distribution section. The primary low-voltage air curcuit breaker will be located in the unit substation supplying power to the rectifier. These rectifiers could be supplied from either the 13.8-kv circuits, 4,160-volt, or the 480-volt circuit. An over-all study of the cost of feeders, switching equipment, and transformer capacity must be made in each case to determine the most economical installation. In Figure 4, four 300-kw sealed tube ignitron rectifiers are shown. Each rectifier unit furnishes

the d-c power requirements for two of the eight mill sections. The rectifiers are operated from the 480-volt unit substations which are located along side the rectifiers in the same control room.

The d-c distribution section in each rectifier will provide the protective circuit breakers for supplying two sections of magnetic separators. These circuit breakers should be of the type used for field circuits of large a-c generators and should have a discharge contact for the highly inductive magnetic separator coils. They can also be electrically operated for remote control from the operating floor. Ground detector lamps on the panels will be provided for indicating grounds in the magnet coils.

Crushing Plant Power Distribution

The location of the crushing plant is a factor in the selection of the circuit arrangement to be used. In Figure 4, it is assumed that the entire crushing plant is located adjacent to the mill.

The power supply to the crushing plant must have the same degree of reliability as for the concentrator plant. In Figure 4, the crushing plant power is supplied at 4,160 volts from one of the 3-phase unit substation transformers. The large crusher motors are supplied directly at 4,160 volts and the smaller motors below 200 horsepower are supplied at 460 volts from a load center unit substation.

When a central crushing plant first is built, with capacity for several mill units, then the power requirements and the load locations may be such that a 13.8-kv circuit to the crushing plant is more desirable.

Motors and Control for Crushing Plant

High-voltage air break contactors have been designed for severe duty cycles such as in mine hoist service and will require a minimum of maintenance in the operation of large crusher motors where frequent starting and stopping and reversing is encountered. In the past, electrically operated oil circuit breakers have been used for this service. However, under frequent operations, the maintenance of latching mechanisms and circuit breaker contacts is a considerable item.

The large gyratory crushers such as the 60-inch size will require two motors of the motors of the wound rotor type, each motor driving the main gear on the crusher through its own pinion. The two motor stator windings can be connected in parallel to one set of reversing air break contactors. Four-pole secondary contactors will provide separate balanced accelerating steps for the rotor circuits of the two motors. Definite time limit acceleration is provided by means of magnetic time delay relays.

Gyratory crushers of the 24-inch size also will be driven by wound rotor motors and will use a control similar to that for the 60-inch crusher except designed for a single motor.

The short head crushers are usually driven by squirrel cage induction motors. Full voltage starters using air break contactors are shown for these motors.

All control units with air break contactors will be protected by current-limiting fuses of 250,000 kva interrupting capacity at 4,160 volts.

Conveyor Belts

A large number of conveyor belts of varying width, length, speeds and lift are required to transport the ore through the various stages of crushing, grinding and concentrating. For the smaller belts, requiring 100 horsepower or less to drive excellent results have been obtained with high torque squirrel-cage induction motors and full voltage starting. The high starting torque motors, such as type *KG*, have approximately 200 per cent starting and 200 per cent pull-out torque, which is usually ample for starting the belt under all operating conditions. The use of normal torque motors often results in overmotoring the drive with resulting higher belt stresses.

Conveyors requiring motors of 200–300 horsepower usually are equipped with belts operating under high stresses. To limit the starting and accelerating torque on these belts, wound rotor motors with secondary control providing automatic time limit acceleration are recommended.

On extremely large belts operating at high stresses, where starting and accelerating torques must be held to a minimum, a large number of accelerating contactors would be required. A number of installations are now in successful operation using a standard 13-point motor-driven drum controller to provide definite time limit acceleration in place of the contactors.

A magnetic time relay provides a means of adjusting the time interval between the first several accelerating points. Control circuit cams on the drum controller provide for sequence starting of the next unit, or sequence starting between two motors operating in tandem on the same belt, and for the release of hold-back devices that may be used. The controller is interlocked with the primary starting switch so as to start only when on the first controller point.

Agglomerating Plant

To complete the picture of the beneficiation plant, a brief review is in order, of the two methods of agglomerating the concentrate, namely, sintering and pelletizing.

Sinter Plant

In the sintering process, the powdered concentrate is mixed with 4–6 per cent pulverized coke or coal, and the mixture distributed on a long slow moving chain grate.

The mixture is ignited and combustion is maintained by large induced draft fans connected to suction boxes under the grate. After leaving the grate, a water quench produces a suitable furnace product in the form of clinker.

The power supply for a sintering plant has been included in Figure 4. Large synchronous motors on the fans constitute the major part of the load. In the proposed plan for one large plant, these motors are rated 900 horsepower. The motors must be 4,000 volts in ratings above 700 horsepower, 0.8 power factor for full voltage starters using current-limiting fuses.

The 460-volt load center power distribution system is similar to that shown for the concentrator plant.

The use of a tie circuit also on the 4,160-volt sinter plant thus provides power for the sinter plant on loss of a feeder or transformer unit. Three transformer units may be operated in parallel if necessary for carrying the entire load without exceeding the 250,000-kva interrupting level.

Pelletizing Plant

Experimental work is being carried on and pilot plants now are being built to produce in commercial quantities, a furnace product from the powdered concentrate by means of the pelletizing process developed in the Minnesota Mines Experiment Station. This process holds great promise of reducing the cost over that now obtained in ore concentrating plants using the sintering process.

The pelletizing process consists of mixing 1.25–1.5 per cent finely pulverized coal with the powdered concentrate along with the proper moisture and balling up this mixture in a revolving drum.

The wet balls, 1/2 inch to $1^1/_4$ inch in size, then are charged in a vertical shaft furnace where the temperature is maintained slightly under the fusion point of the concentrate.

No complete data are as yet available on the power requirements for large pelletizing plant units. From available data it appears that the maximum motor horsepower ratings will be within the limits of 460-volt distribution. On this basis, the power distribution would consist of load center unit substations 13,800/480 volts with a primary arrangement and secondary selective system as in Figure 4, for the 460-volt power distribution in the concentrator plant.

Lighting Distribution

A combined lighting and power system will provide low first cost and excellent over-all service reliability in the plant areas. In this system, small dry-type transformers are connected to 460-volt feeders on the load center unit substations to provide the lighting power requirements. The use of separate transformer substations and feeders for lighting, especially in buildings such as the concentrator which may exceed 600 feet in length, would result in considerably higher first cost and less service reliability.

In areas remote from the mill where lighting and small motor loads are encountered, such as large offices and mill laboratories, 208Y/120 volt service is advantageous since both single-phase and 3-phase power is available for both lighting and small motors.

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No Discussion

Push Button Switching in Telegraph Systems

R. F. BLANCHARD
NONMEMBER AIEE

W. B. BLANTON
NONMEMBER AIEE

PAPERS presented before the Institute in previous years have described broadly the methods by which telegraph communications are handled between patrons located in widely separated points, and in particular have outlined the system employed in the more modern offices for relaying messages between incoming and outgoing circuits. Because consecutive messages arriving on an incoming circuit are usually directed to different and widely separated destinations, one of the most costly and time-consuming phases of telegraph operations has been the separation of a continuous incoming stream of telegraph messages into its separate components, sorting out these separate messages and then starting them out again, each to its own separate destination.

The modern system of performing this complicated relay operation, the reperforator switching system, has been described in previous papers[1,2] in its original form which employed the plug and jack type of switching. A more recently developed type of reperforator switching system is that utilizing push button control, which Western Union installed in its Philadelphia and Cincinnati offices in 1947. This system employs the same fundamentals of reperforator-switching as the original system, but the method of effecting the intraoffice connection is quite different. A special adaptation of this system for private line telegraph systems was described in a recent paper.[3] A more highly developed form of push-button reperforator switching suitable to the needs of commercial telegraph offices is described in this paper. Figure 1 shows one of the push-button switching aisles in the Philadelphia office.

Principal Elements of a Reperforator Switching Unit

A switching system may be considered as consisting of three principal parts:

1. Line receiving printer-perforators to receive line signals and record them in the form of printed and perforated tape.
2. Line sending transmitters to transmit line signals from a perforated tape.
3. Intraoffice switching facilities which includes transmitting and perforating apparatus for distributing signals from the received printer perforator tapes to the various line transmitters, each of which serves one channel of an individual outgoing circuit.

There is little of a technical nature that is new in items (1) and (2), except the printer-perforator used at line receiving positions. Machines for automatically perforating tape, from received signals, and for automatically retransmitting equivalent signals from such tape have been used in the telegraph industry for many years. The printer-perforator, however, not only perforates the code selection characters in a paper tape, but also prints the corresponding characters on the same tape in order to facilitate reading by switching clerks. The heart of a reperforator-switching system lies in the intraoffice switching facilities to which the major part of this paper will be devoted.

Line Receiving Positions

A line receiving position is equipped with a printer-perforator, and also with its associated portion of the intraoffice switching apparatus. Such a position has a printer-perforator which receives from a line circuit, an intraoffice transmitter through which the tape produced by the printer-perforator flows (one message at a time with each switching operation), and a tape accumulator mounted between the printer-perforator and the transmitter, into which the tape from the printer-perforator feeds, and from which the tape is withdrawn as the transmitter functions. Three heavily loaded, or five lightly loaded, receiving positions are mounted adjacent to a push-button switching turret. Figure 2 shows a 5-position turret.

Line Transmitting Positions

Transmitting positions are equipped with line sending apparatus and associated intraoffice switching apparatus. In addition to the line transmitter which is continuously connected to its transmitting circuit, there is a reperforator (non-printing) which receives signals over an intraoffice circuit from the various intraoffice transmitters, a tape accumulator like the one described for receiving positions and an automatic numbering machine. Tape from this intraoffice reperforator flows continuously through the line transmitter as long as there is tape on hand to transmit (Figure 3).

Intraoffice Switching Operation

When a telegram is received at a line receiving position, a switching clerk notes the destination of the message as printed on the tape produced by the printer-perforator (Figure 4) and determines from memory or from a route chart the circuit over which it should be retransmitted. The clerk then depresses an "initiate" push-button which is located adjacent to and on the same shelf with the intraoffice transmitter, which is associated with that tape

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R. F. BLANCHARD and W. B. BLANTON are both with the Western Union Telegraph Company, New York, N. Y.

Figure 1. A push-button switching aisle at Philadelphia, with type 36-A chadless tape printer-perforators

Figure 2. A 5-position switching turret, with type 36-A chadless tape printer-perforators

(Figure 5). The initiate push-button conditions that transmitter for the switching operation. The "destination" push-button in the switching turret corresponding to the desired outgoing circuit is then depressed. The push-buttons in the switching turret control rotary switches, through which a potential connection is established between the conditioned transmitter and the selected intraoffice circuit. This potential connection is not converted into an actual connection, until a transmitter allotter has functioned to test a path of that intra-office circuit and has found it to be idle. Then the actual connection is established through that path of the intraoffice circuit into a reperforator associated with the desired transmitting circuit, either directly in the case of a single-channel circuit, or through the functioning of "transmitter finder" and "load distributor" switches in the case of multi-channel circuits to the desired destination.

When an actual intraoffice connection has been established between a transmitter at a line receiving position and a reperforator at a line transmitting position the intraoffice circuit is immediately seized by an automatic numbering machine which transmits into the intraoffice reperforator the correct message sequence number for that sending channel. After the operation of the automatic numbering machine, the intraoffice transmitter sends its telegram. All telegrams are terminated with a special end-of-message signal, consisting of two periods(..) which causes the intraoffice transmitter to stop and to be disconnected from the intraoffice circuit, thereby clearing that circuit for use by any other intraoffice transmitter which may be seeking a connection to it. The message has now been transferred from the printer-perforator tape of the receiving position to the reperforator tape at the sending circuit terminal. As described previously retransmission of the message to the outgoing circuit is accomplished by this perforated tape automatically flowing through the line transmitter.

Intraoffice Switching Facilities

Circuits. Nine conductors are involved in establishing an intraoffice connection. Three of the conductors perform functions required in establishing the connection, in providing supervisory signals, and in disconnecting, while the other six conductors are used in transmission. The use of six conductors, one of which is used for the transmitter step pulse, makes possible the simultaneous transmission of all five of the code pulses for one character. This enables the intraoffice apparatus to be operated reliably at a transmission speed of 15 characters per second (equivalent to 150 words per minute) with little maintenance attention.

Intraoffice circuits differ depending upon whether they serve an outgoing circuit which has but a single channel or a circuit which has several channels.

Figure 6 is a block diagram which shows two types of intraoffice switching circuits, one for a single-channel circuit designated as *AB*, and the other for a 3-channel circuit designated as *BC*.

Figure 3. A transmitting position with intra-office reperforator and line transmitter

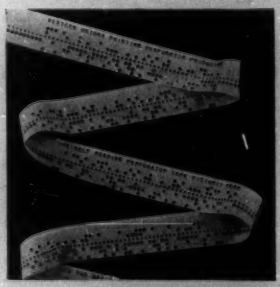

Figure 4. A section of printer-perforator tape, from a 23-A chad-tape type printer-perforator

Figure 5. A receiving position with line printer-perforator of the 23-A chad-tape type, and intraoffice transmitter

Figure 7. Impulse unit sub-base equipped with one impulse unit

The intraoffice reperforator shown on the single sending channel of circuit *AB* terminates an intraoffice circuit that provides only one path for the whole office over which the intraoffice transmitters can reach the *AB* sending position. If two or more intraoffice transmitters are switched to such an intraoffice circuit simultaneously or at approximately the same time, the transmitter allotter, to be described later, will function to permit only one transmitter at a time to receive an actual electric connection while all other transmitters are held in a waiting condition with potential connections.

Where there are two or more channels for a sending circuit, the intraoffice circuit is on a multipath basis. Generally 25 or less paths are provided. Each path, shared by a relatively small group of intraoffice transmitters, terminates in a group of transmitter-finder switches (Figure 6), which are associated with the channels of the sending circuit. These transmitter-finder switches function to connect any one of the intraoffice paths serving that circuit to any one of the channels. If two or more intraoffice transmitters have potential connections at the same time to the same path of an intraoffice circuit, the transmitter allotter functions to permit only one of these transmitters at a time to seize this path. When an intraoffice transmitter has seized a path, the transmitter finder switch of an idle channel will function to complete an actual connection between the intraoffice transmitter and this sending channel position. Therefore, in the case of a 3-channel circuit such as *BC*, three intraoffice transmitters, (if each has access to a different intraoffice path) may send simultaneously into the three intraoffice reperforators which serve the three channels of the circuit *BC*.

A load distributor, common to all of the channels of a multichannel circuit is provided to distribute as evenly as possible, the messages into each of the channels of a multichannel circuit. For example, in the case of circuit *BC* in Figure 6, the load distributor would direct the first message to channel A, the second to channel B, the third to channel C, the fourth to A, and so on, except that when a channel is found busy, the load distributor will by-pass that channel and direct the message to the next idle channel.

Impulse Unit. An impulse unit furnishes the five selection, the step, and the punch pulses for transmitting characters into the reperforator. It consists of a constantly rotating shaft equipped with cams and co-operating contacts, Figure 7. Each cam has two lobes. In each revolution, the co-operating contacts are closed twice, the time and length of closure being determined by the position and length of the lobes. One character is transmitted per half revolution, therefore for 15 characters per second (150 words per minute) the impulse unit is operated at $7^1/_2$ revolutions per second. The timing chart of the closure time for the contacts during the transmission of one character in one-fifteenth of a second ($0.066^2/_3$ second) is shown in the upper right hand corner of Figure 8.

Transmission of the code characters in the tape as it passes through a transmitter is accomplished in the following manner. (See Figure 8.) The step pulse from the impulse unit energizes the step magnet of the transmitter. The operation of the step magnet armature

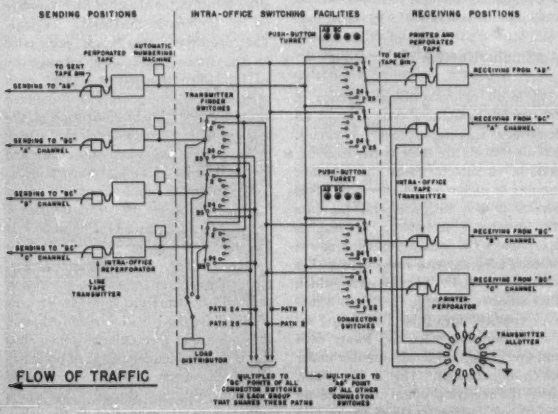

Figure 6. Switching to single-channel and multichannel circuits

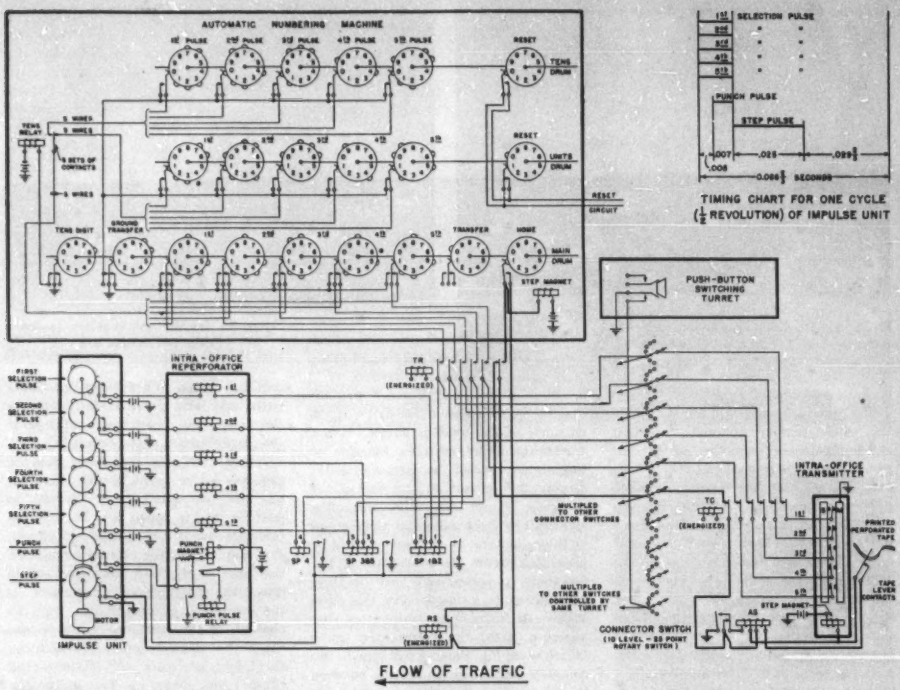

Figure 9. Intraoffice transmission

pulls the feeler pins down and causes their co-operating tongues to move to the spacing bus bar and steps the tape forward to the next character. At the end of the step pulse, the step magnet de-energizes and releases the feeler pins. Each feeler pin that encounters a hole in the tape will project through that hole and its co-operating tongue contact will move to the marking bus bar and thus apply a ground on the associated selection pulse conductor for a marking signal. Each feeler pin that encounters no hole in the tape will be restrained by the tape and its co-operating tongue contact will remain on the spacing bus bar which is ungrounded and thus the associated selection pulse conductor is set up for a spacing signal. The momentary closure of the five selection pulse contacts of the impulse unit will cause a momentary flow of current through the intraoffice reperforator selection magnets that are included in the conductors that are grounded at the intraoffice transmitter. While the reperforator is perforating a received character code, in response to the punch pulse from the impulse unit, the step pulse causes the transmitter to advance its tape to the next character and sets up its feeler pins to correspond to the code combination of that character.

Automatic Numbering Machine

It is an old established practice in telegraphy to number consecutively all telegrams transmitted over a circuit. Number sheets are used at the receiving ends of circuits to check the sequence number of each received telegram. This insures proper reception and prevents duplication of telegrams. In manual operation, a transmitting operator marks on each telegram with a pencil, or with a number stamp, the number under which that message is sent. Therefore, whenever circumstances necessitate doing so, the transmitting office can locate each and every telegram by the number under which it was sent. This is important, since the receiving office may have no record of having received a certain number, or numbers, and would have no means of describing to the transmitting office the omission of a number.

In reperforator-switching operation, these message sequence numbers are automatically transmitted into an intra-office reperforator ahead of each message by an automatic numbering machine. This operation takes place just prior to the transmission of a message by an intraoffice transmitter. Thus, each message and its associated sequence number appear in the same tape at the sending position and all telegrams may be located by their numbers in the same way as described for manual operation.

The automatic numbering machine is a form of transmitter that sends a series of predetermined characters (Figure 9). It functions in a way somewhat similar to a tape transmitter. Whereas the tape transmitter determines the code combination that will be transmitted by feeler pins "feeling" for the holes in perforated tape, the automatic numbering machine has the code combinations for its predetermined characters set up by means of studs in rotating drums. The studs actuate contacts connected to the selection pulse conductors; the presence of a

stud actuates its co-operating contact to apply a ground for a marking signal; the absence of a stud leaves its co-operating contacts open; therefore no ground is applied and a spacing signal is transmitted.

Transmission from an automatic numbering machine into an intraoffice reperforator is accomplished by selection, punch and step pulses from the impulse unit, in the same manner as previously described for the intraoffice transmitter.

Transmitter Allotter

When a potential connection is set up between an intraoffice transmitter and an intraoffice circuit, a circuit is established from the intraoffice transmitter to its associated contact on the transmitter allotter. The latter has 188 contacts arranged in a circle (Figure 10). Inside of the circle is a constantly rotating brush arm that connects ground to the 188 contacts one at a time. This particular allotter makes one revolution every $3^3/_4$ seconds. Each intraoffice transmitter circuit is connected to an individual contact on the allotter. Thus every transmitter waiting to seize an intraoffice path has a momentary ground applied to its circuit once every $3^3/_4$ seconds.

When the transmitter allotter applies ground to the transmitter circuit, a pulse of current will flow if the path of the intraoffice circuit is idle. The flow of current causes the transmitter to seize the intraoffice circuit path and immediately "busies" the path to all other transmitters.

If a second transmitter is endeavoring to seize a busy intraoffice path no current will flow when the transmitter allotter applies ground to the circuit of this second transmitter. Thus the second transmitter cannot seize the intraoffice path until the first transmitter has completed transmission of its message.

When that happens, the next time the transmitter allotter tests through the circuit of the second transmitter, a current flow will result and the second transmitter will seize that intraoffice circuit path.

When an intraoffice transmitter seizes the intraoffice circuit path of a single channel sending circuit, it immediately obtains an actual connection to the intraoffice reperforator on the sending position. However, in the case of multichannel sending circuits, the seizure of any one of the several intraoffice circuit paths sets up a calling condition and the actual connection to an idle sending position is completed by the operation of the load distributor and a transmitter finder switch.

Figure 9. Automatic numbering machine

Push-Button Switching Turrets

As previously indicated, each switching turret serves three or five intraoffice transmitters. The circuit arrangements are so interlocked that only one transmitter of the group of three or five transmitters served by the same turret can be conditioned for a switching operation prior to the act of depressing a turret push-button. This prevents inadvertently conditioning two transmitters at the same time and then connecting the first transmitter to the destination intended for the second transmitter or vice versa.

An "initiate" push-button is associated with each intraoffice transmitter. Momentarily depressing the initiate button of any one of the three or five transmitters served by one turret will condition that particular transmitter circuit through an arrangement of relays so that it will be the next transmitter at that turret to be switched when one of the destination push-buttons in the turret is depressed.

Only one push-button is provided in each turret for each destination. If two or more intraoffice transmitters at the same turret have messages for the same destination, for example Chicago, the switching clerk would depress the "initiate" push-button of one of the transmitters and then depress the Chicago "destination" push-button in the turret. Then the clerk would depress the "initiate" push-button of another transmitter and again depress the Chicago "destination" push-button in the turret. The same procedure would be followed for other transmitters having Chicago messages. These potential connections to the Chicago sending position or positions will be automatically converted to actual connections by the operation of the transmitter allotter, transmitter finders, and load distributor.

Rotary Switches

The rotary switches which constitute the medium for making the connection between the intraoffice transmitters and the intraoffice circuits, have a rotor, comprising a 10-arm wiper assembly, which can be stepped to any one of 25 positions. The stator portion of these switches is equipped with 10 contacts at each of 25 positions. Figure 11 shows two types of rotary switches which operate similarly although different in mechanical construction. Both types are used interchangeably in push-button switching.

The nine conductors of an intraoffice transmitter circuit are connected to nine of the wiper arms of the rotor of a rotary switch. When the rotor is stepped to any one of 25 positions on the rotary switch, the 10-wiper arms make contact with the ten stator contacts at that position. The nine conductors of intraoffice circuits are connected to nine stator contacts on one of the 25 positions of a rotary switch. The tenth stator contact of each of these positions is connected to one of the push-buttons in the switching turret.

Depressing any one of the destination push-buttons in the switching turret will put a "marking condition" on the tenth contact of the rotary switch at the position where the desired intraoffice circuit is connected. The rotor of the rotary switch, by the action of the electromagnet, the stepping pawl, and ratchet wheel, will self-step until it finds this marked condition on the tenth contact. The rotor will then come to rest at that

Figure 10. Transmitter allotter

position on the switch. This connects the nine conductors of the intraoffice transmitter to the nine conductors of the intraoffice circuit through the rotor and stator contacts on nine levels of the rotary switch.

When a turret push-button is depressed, it prepares two circuits, one for actuating the step-magnet and one for marking the position of the rotary switch contact bank where the rotating contacts, termed "wipers", are to stop stepping. The circuit through one pair of contacts of the push-button extends through the rotary switch step-magnet, and the back contacts of a relay to battery. That circuit actuates the step-magnet armature which causes the stepping pawl to engage the next tooth of the ratchet wheel and also causes a pair of contacts associated with the armature to close. The closure of the latter contacts completes a circuit for the operation of the relay which opens the circuit through the step-magnet and causes the armature to release. The release of the armature advances the wipers one step and reopens the armature contacts which release the relay. The release of the relay re-establishes the circuit condition which existed when the push-button was first depressed. Another operation will occur as just described and those operations will repeat until the marked position of the rotary switch is reached by the wipers. When the position is reached a circuit will be completed through a second pair of contacts of the push-button, a stator contact and one of the wiper contacts of the rotary switch, a second winding of the relay to battery. That circuit holds the relay operated and thereby prevents re-establishing the operate circuit through the rotary switch step-magnet. Thus, the rotary switch wipers are caused to be advanced over the switch positions and stopped when the marked position is reached.

Direct and Sharing Switching

Economies were obtained in the push-button switching equipment used in the Philadelphia and Cincinnati offices by using a combination of "direct" and "sharing" switching arrangements. Each intraoffice transmitter is provided with four rotary switches individual to that transmitter, by means of which it can be switched directly to 72 heavily loaded destinations, and can be switched to rotary switches, common to 11 or 13 transmitters, for reaching 198 destinations. The intraoffice circuits for heavily loaded sending destinations are terminated on the stator positions of the rotary switches which are individual to the transmitters while the intraoffice circuits of the lightly loaded sending destinations are terminated on the stator positions of the sharing rotary switches. Figure 12 shows diagrammatically the direct and sharing rotary switch arrangement for a group of 13 transmitters.

Figure 11. Two types of rotary switches

The transmitter is connected to the rotor of the direct selector switch. The intraoffice circuits of 22 destinations are connected to the first 22 positions of this switch. The twenty-third position is connected to the sharing selector switch, the twenty-fourth position to the *A* direct connector switch and the twenty-fifth position to the *B* direct connector switch. Thus; through the direct selector switch and the direct connector switches *A* and *B*, the intraoffice transmitter can be switched to any one of 72 destinations through equipment which is individual to the transmitter. With these direct switching facilities, all 13 transmitters can be switched to the same destination or to 13 different destinations out of the 72. Sharing connector switches 1 to 24 are provided for the remaining 198 destinations, which gives a total of 270 destinations, the capacity of the push-button switching turrets.

An intraoffice transmitter is connected to one of the destinations terminated on the sharing switches by its associated direct selector switch advancing to the twenty-third position, by its associated sharing selector switch advancing to the proper position to make connection with one of the sharing connector switches which serves that destination, and by that sharing connector switch advancing to the position to which the intraoffice circuit of that destination is connected. All of these switches self-step simultaneously and their action is controlled by a destination push-button, depressed in the push-button switching turret.

From the foregoing description it will be seen that in the push-button reperforator-switching system, telegrams are received on printer-perforators which record them on a paper tape in the form of printed characters and perforated holes corresponding to the printing telegraph code. These printed-perforated tapes are passed through intraoffice transmitters that transmit the messages across the office where they are received on intraoffice reperforators which again record them on a paper tape in the form of perforated holes corresponding to the printing telegraph code (but not printed) and this latter tape is passed through a line transmitter which transmits the signals over an outgoing circuit to a distant office. The intraoffice transmitter conductors are connected to the conductors of any desired intraoffice circuit through the contacts of rotary switches which in turn are controlled by push-buttons in switching turrets. Intraoffice transmitters, intraoffice reperforators, and automatic numbering machines function under control of impulse units. The transmitter allotter functions to allow but one intraoffice transmitter to be connected to a given intraoffice circuit at a time, and automatically establishes a new connection to an intraoffice circuit as quickly as preceding connections have been released; and each intraoffice connection is automatically released just as soon as the intraoffice transmitter sends the last character of the telegram for which the connection was established. These are the fundamentals of push-button reperforator-switching.

There are many supervisory controls used in this system that are beyond the scope of this paper to be described in detail. Various signal lamps and buzzers function to warn switching clerks, supervisors, and regulating attendants of unusual conditions. A broadcast system of microphones and loud-speakers is provided in order that switching aisles, transmitting aisles, supervisors, and regulating attendants may have instantaneous communication with one another as needed. Special book switching positions are used for assorting and retransmitting multiple-address telegrams where a single copy of the text is to be transmitted to a large number of addressees. There is a master key transmitting position from which periodic market quotation messages are transmitted simultaneously into as many outgoing circuits as desired. Special switching positions are provided for switching to "tie-line" patrons in the same city.

Western Union's mechanization program calls for 15 major offices to be

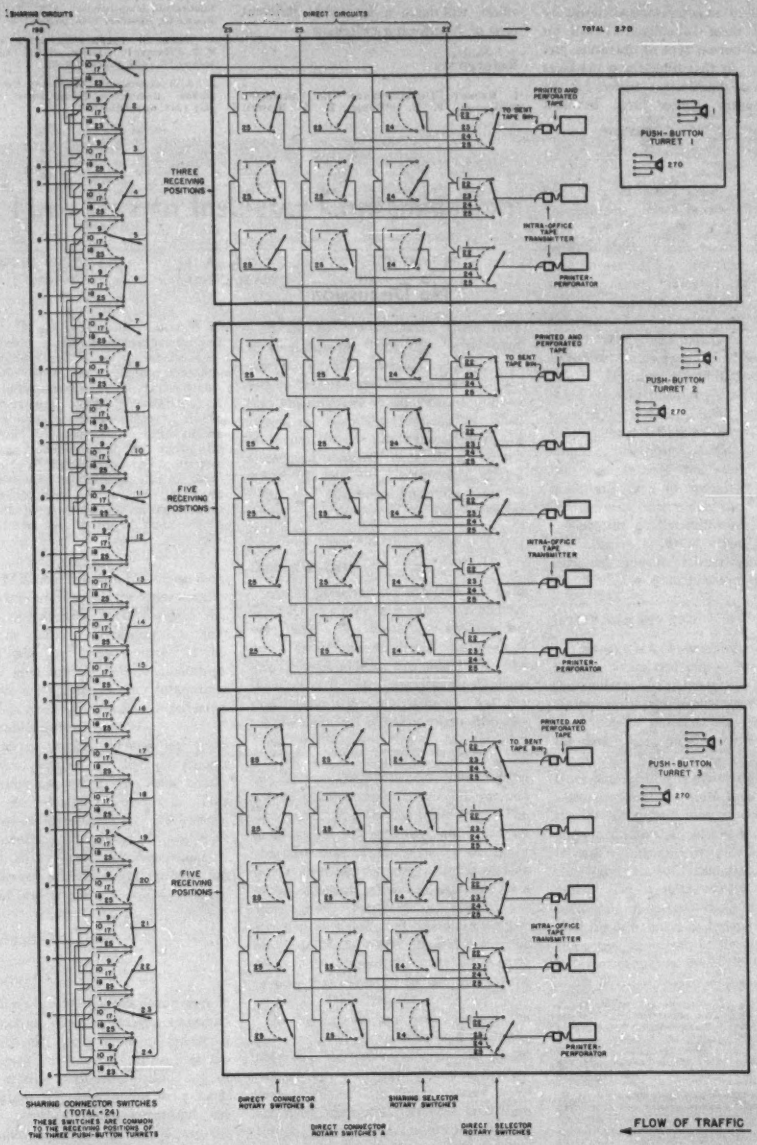

Figure 12. Direct and sharing rotary switch arrangement

converted to reperforator-switching, by 1950. Of these 15 offices, 11 will include push-button type of operation just described. At that time, these 15 offices will serve as distribution centers for the entire country. Other large terminal offices will have a somewhat different type of reperforator-switching.

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No Discussion

Pole Fires Due to Insulator Contamination

W. H. WICKHAM
MEMBER AIEE

H. A. ADLER
ASSOCIATE AIEE

M. S. OLDACRE
FELLOW AIEE

Synopsis: Wood poles and crossarms of overhead electric lines may become ignited and damaged when the surface of the insulators and wooden parts become contaminated by industrial dust and upon the occurrence of certain atmospheric conditions. A study was made in the laboratory of all the factors which might contribute to the causes of pole fires. Conditions of dirt and fog and also burning of poles similar to occurrences in the field were reproduced. An explanation of the causes of pole fires was found and finally a simple and effective method of preventing the burning was developed.

WHILE pole fires are not unusual on overhead electric lines, there usually is little opportunity to investigate the causes or propose remedies. A few cases of pole fires have occurred in the Chicago area and under circumstances that gave a basis for further laboratory study that might be of value in reducing such troubles in the future.

The circumstances surrounding these fires were peculiar in that on similar lines, under apparently the same conditions, no burning occurred. This prompted an investigation of the various factors contributing to pole burning in an attempt to evaluate the importance of the different variables and to develop means of prevention.

Analysis of Variables

Contamination

The lines involved in this study were in an industrial district where contamination with dirt from adjacent industrial plants was very heavy. The fires occurred during periods of heavy fog or light drizzle following prolonged periods of dry weather which permitted the accumulation of contaminants on the line structures. This agrees with other investigators' findings that pole fires are caused by contamination and fog.

In the case investigated there were wooden poles on both sides of a street carrying 33,000-volt lines. While there was no reason to believe that contamination was different on the two sides of the street, the observation was made that burning occurred only the east side of the street and none on the west side of the street. It was obvious, therefore, that a number of additional variables must be contributing factors in the production of pole fires.

Design of Line

At the present time the poles on both sides of the street each carry two 33,000-volt lines. The structures are wooden poles with three wooden crossarms, one line on each side of the pole. The two lines on each pole have the same phase designation for the conductors on the middle arm and different phase designations for the conductors on the top arm and on the bottom arm, that is, on a specific pole the phase arrangement from top to bottom might be *A B C* for one line, and *C B A* for the other line. The observations showed that the lines on the east side of the street were noisy due to electrical discharges during periods of fog or drizzle while the west side lines were quiet. Furthermore, careful inspection revealed that the lines on the east side of the street showed numerous cases of tracking on the arms, while little or none was observed on the lines on the west side of the street.

At the time the pole fires occurred, the poles involved carried two lines while the poles on the other side of the street carried only one line. From theoretical considerations, the double-circuit poles are more susceptible to pole fires than the single-circuit poles under the existing phase arrangements, because the leakage current density is greater for double-circuit poles than for single-circuit poles, especially in the middle gain, that is, the contact surface between the middle arm and the pole. That pole fires tend to start at this point agrees with field observations. Further investigations, however, indicated that this was not the complete explanation for the difference in behavior of the lines. Shortly after these pole fires occurred an additional line was installed on the west side of the street, so that from then on there were double-circuit poles on both sides of the street. While there were no subsequent pole fires on either side of the street, it was observed that the lines on the west side of the street remained quiet and free of tracking while discharges were observed on the east lines.

Since the constructions are so similar, it became apparent that additional variables must have been responsible for the difference in the behavior of the lines.

Insulators and Pins

The next set of variables studied were the insulators and pins. Practically all the insulators on the east lines were of one design, while the insulators on the west lines were of different designs including the design used on the east lines. Furthermore, all pins on the east lines were truss pins while the standard round pins were used on the west lines. Because the insulators and pins on the east lines were predominantly of one type, the first laboratory studies were made on the characteristics of insulators and pins alone. The results of the laboratory tests, to be described later, indicated that the differences in design of insulators were not sufficient to account for the different performances of the lines.

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W. H. Wickham and H. A. Adler are senior engineers with the Commonwealth Edison Company, Chicago, Ill., and M. S. Oldacre is director of research, Utilities Research Commission, Chicago, Ill.

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Table I

Type of Insulator	Fog-Wet Flashover, Kv: Field Dirtied, Not Energized	Fog-Wet Flashover, Kv: Field Dirtied, Energized	Fog-Wet Flashover, Kv: Artificially Dirtied, Energized	Fog-Wet Surface Resistance, Megohms: Field Dirtied, Not Energized	Fog-Wet Surface Resistance, Megohms: Field Dirtied, Energized	Fog-Wet Surface Resistance, Megohms: Artificially Dirtied, Energized
4	37–46.5	34	29	0.37–.70	0.22	0.40
5	29–30.5		31	0.18–0.30		0.24
6	30–36		24	0.22–0.58		0.17
7	34	35	28	0.51	0.27	0.46

Wood Structures

As a next step in the study of variables affecting pole fires, attention was concentrated on the treatment of the wood structure. A survey revealed that all poles were creosoted on the east lines while only three per cent of the poles were creosoted on the west lines. Furthermore, all arms were creosoted on the east lines while only two-thirds of the arms were treated on the west lines. The laboratory studies, therefore, proceeded to include the wood members with various combinations of treatment. The results showed that the treatment is an important factor in producing pole fires.

Laboratory Studies

Insulators and Pins

In the study of insulators under conditions of contamination and fog, a procedure was used in the laboratory similar to the one described in another paper.[1] The insulators were dirtied with a mixture of fly ash and Portland cement by a method of alternate fogging and dusting while energized at 19 kv which is the line-to-ground voltage in service. The amount of dust and the directions of the nozzles were so adjusted as to give a pattern and degree of contamination similar to that observed in the field. Except for the direction of the nozzles, the arrangement for contamination and fogging was similar to the one shown in Figure 1 of the paper just mentioned. The nozzles were modified so that the top nozzle formed an angle of 45 degrees with the vertical and the lower one an angle of 30 degrees. The top nozzle was directed upwards against the rim of the top shed and the lower one into the recess around the pin.

This method gave closely reproducible results as determined from electrical characteristics. The amount of the contamination was adjusted so that the fog-wet flashover was approximately 28 kv or $1^1/_2$ times the phase-to-ground voltage in service. This degree of contamination was established previously from service experience as a serious condition. Table I compares test results for artificially-contaminated insulators with field-dirtied insulators which had been installed on the same poles that carried the 33-kv lines or on adjacent poles. Some of these insulators were energized while mounted on the poles in the field and some were not energized. It can be seen from Table I that the effect of field contamination upon flashover and surface resistance was reasonably similar to the effect of laboratory contamination.

The eight different insulators in operation on the four lines are shown in Figure 1. The effect of dirt and fog on the flashover of these insulators is shown in Figure 2. Insulator 6, which is the one used predominantly on the east lines shows the lowest value under fog and dirt conditions. However, there were other insulators that were not much better. The data show that the type of pin had little effect.

Since pole fires are essentially a leakage problem rather than a flashover phenomenon, studies were made of the surface resistance under fog and dirt conditions. The results are shown in Figure 3. Insulator 6 was the poorest in these tests. However, where the contamination has reached a degree so that sufficient current flows to start a pole fire, the surface resistance of the insulator is so low that it is only a small part of the leakage resistance of the insulator-wood circuit. While the lowering of the resistance of insulators due to contamination is an important factor in creating pole fires, the differences between the resistances of the contaminated insulators of the eight different types tested appear of little significance. The marked difference between insulator 1 and the others is due to the absence of a metal thimble in this insulator.

Under fog-dirt conditions the flow of leakage current is not uniform. Milliammeter charts showed current pulses varying over wide limits. These variations were caused by drying of portions of the insulators under the effect of current flow and subsequent arcing over

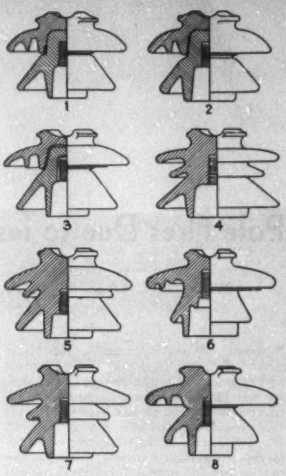

Figure 1. Types of 33-kv insulators covered by investigations

Diameter over widest petticoat $9^3/_4$ to $10^1/_2$ inches; height 7 to $7^1/_2$ inches

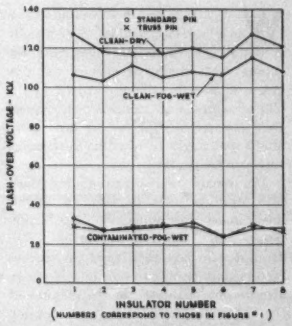

Figure 2. Flashover voltages of 33-kv pin-type insulators

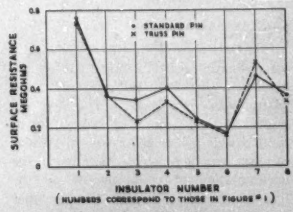

Figure 3. Surface resistances of 33-kv pin-type insulators (contaminated, fog-wet)

these dried portions. These current-time charts did not show any consistent differences between the various insulators. Similarly, measurements of the total energy flow by means of a sensitive watt-hour meter indicated no significant differences between the insulators.

Pole Fire Tests

In order to study the various factors contributing to pole fires, a laboratory setup was made reproducing, as closely as practical, the actual service conditions. Pole fires were produced under conditions of dirt and fog, resulting in burning similar to that which has been observed in the field.

The laboratory setup consisted of a section of pole, a crossarm, and an insulator. A gain was cut in the section of pole and the crossarm bolted to it. A second bolt through the pole removed some distance from the gain was the ground electrode. The length of crossarm in the circuit varied from $1^1/_2$ to 5 feet and of the pole from 1 to 3 feet in the series of tests. The insulator, contaminated with fly ash and cement mixture, was mounted on a metal pin on the crossarm. Wet water vapor, supplied by low pressure steam, was directed at the pole assembly to simulate fog conditions. This was sometimes supplemented by a fine water spray from an atomizer.

The procedure in producing a pole fire was as follows. The test voltage of 19,000 volts was applied between the top of the insulator and the grounded bolt in the pole. This voltage is the line-to-ground voltage on the 33,000-volt transmission lines in service. Fog was directed to strike the insulator first, then envelop the crossarm, and finally cover the pole between through bolt and grounded bolt. Current was measured by a recording milliammeter connected in the ground lead.

The observations and measurements made in a large number of such tests, not all of which resulted in pole fires, show the importance of the various factors which in combination produce pole burning.

The serious type of fire is the "pocket fire." The burning is most often a smoldering without open flames but the result is a deep "pocket" burned in the pole, and some burning of the crossarm. Burning may occur also on the opposite side of the pole at the through bolt, but only one such case was observed in all the tests.

The magnitude of current necessary to produce a pole fire cannot be specified. In some tests fires occurred with currents as low as 5 milliamperes. In other tests currents considerably in excess of 5 milliamperes did not initiate fires. It is not the magnitude of current but the concentration, or current density at some point usually in the region of the gain, which gives rise to the serious burning.

The immediate cause of burning is an arc in a highly stressed area. The arc must exist at a point where fuel is present in the form of wood sufficiently dry to burn or from the wood impregnant. Such a highly stressed area is found in the region of the gain. Where pole and crossarm meet the wood is relatively dry and furthermore, the current must take a cross grain path in passing from crossarm to pole. The potential distribution curve of Figure 4, plotted from data taken on a test setup which had been subjected to fog, shows that nearly one-half of the voltage drop exists in the short section of the circuit represented by the gain. The relatively dry wood in this area supplies fuel also. The chimney effect created between the pole and crossarm and the mutual heating of pole and crossarm help to sustain the burning. Most of the fires which occurred in the laboratory tests were found to originate at the gain. Field observations are in agreement with the laboratory results. It should also be noted that most, or all of the conditions, set forth as causes for burning may also occur in cracks in the wood and around the hardware. One fire was started on the back of the pole at the through bolt. Cracks were observed to aid the spread of burning.

Not all the tests resulted in pole fires. By means of surface resistance measurements, it was found that current was greatly reduced and burning prevented by drying of the insulator due to heavy leakage current or by creation of sufficiently long areas on the crossarms or poles.

On the insulator, the drying occurred usually in the area around the pin or the cap where the leakage current density was highest. On the crossarm, drying took place in areas of high resistance. Such areas frequently showed arcing which resulted in light burning or "tracking" on the surface of the wood. On short specimens of crossarm this tracking sometimes progressed the length of the arm and resulted in serious burning. On longer specimens, which more nearly represent the field conditions, tracking resulted most often in drying with a reduction in leakage current before serious burning occurred. The pole portion of the wood circuit acts in much the same way.

The treatment, or impregnation, of the wood of pole and crossarm is an important factor in the occurrence of fires. Untreated pole wood was found to absorb surface moisture readily and a plentiful supply of moisture was necessary to produce the low resistance and the leakage current necessary for burning. On wood freshly impregnated with creosote the moisture collected on the surface in more or less isolated droplets, thus limiting the leakage current. However, after exposure to weather the creosote was leached from the outer surface, yet moisture could not penetrate far into the wood. The result on such specimens of wood was a low-surface resistance and high-leakage current in fog. The creosote impregnant also furnished fuel for burning. The occurrence of fires was found to be much more frequent in creosote-impregnated wood than in untreated wood in the laboratory tests. This is in agreement with field observations that under fog conditions the east lines carried on creosoted poles were noisy and subject to tracking.

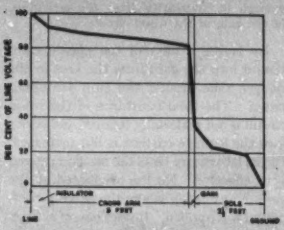

Figure 4. Potential distribution on contaminated fog-wet insulator, arm and pole

Preventive Measures

From a consideration of the factors governing the occurrence of pole fires, it was concluded that an effective method of preventing or minimizing the danger of pole fires would be one which eliminated the arcs in the highly stressed region at the gain, without reducing too greatly the resistance of the gain since it is a large part of the total resistance of the wood circuit.

The method which proved satisfactory was calking the gain lines, that is, the junction lines between pole and crossarm, with a plastic compound. Laboratory studies indicated that the calking compound should have the following characteristics:

1. The compound should be soft and adhesive.
2. It should retain its plastic and adhesive properties under prolonged weathering.
3. It should be of a type which can be applied with a gun as a fillet into the gain lines.
4. It should be slightly conductive but not

enough to materially alter the resistance of the gain of the wood circuit.

By laying a fillet of the plastic compound into the gain lines the arcs in the danger zone around the gain are eliminated. The total resistance of the wood circuit is not materially reduced and therefore the leakage current is not increased. In the laboratory tests the method proved very effective. No fire originated at the gain in any setup where the calking compound was applied. It does not, of course, eliminate the possibility of fires at the hardware or at cracks in the pole, but such fires are in the minority. Application of the compound to existing lines, with calking gun, is simple.

Other suggested methods for eliminating fires at the gain were tried also, but were found by test to be less satisfactory. One method was the installation of a conducting layer in the form of a metal liner in the gain between pole and crossarm. Unless the fit is very good and the metal in close contact with the wood, arcing occurs between wood and metal, much the same as between pole and crossarm in the ordinary gain, and protection is not achieved.

A second method of protection tried was bonding, that is, the wrapping of both pole and crossarm with wire on all sides of the gain and connecting all bond wires together. This method reduces the resistance of the wood circuit by eliminating the resistance of the gain and increases the voltage stress across pole and crossarm, thus increasing the leakage current and the tracking on the surface of the wood members. Furthermore, the bond wires may in time become loose, giving rise to arcing and making a fire possible at the points of application of the bond wire in the same way as at the hardware.

Because of simplicity in application and effectiveness in preventing burning, it was concluded that calking of the gain is the best of the methods tried and will prevent most pole burning.

Conclusions

1. Pole fires cannot be attributed to any single factor but are the result of a combination of conditions to be fulfilled simultaneously.

2. Insulator contamination is a requisite for pole burning. However, eight common types of insulators studied on two types of pins in this investigation did not show sufficient differences under conditions of contamination and fog to conclude that pole burning is much more likely with one than with another.

3. Burning is more apt to occur on creosote-impregnated wood than on untreated wood.

4. Pole fires are most likely to occur at the gain.

5. An effective method of preventing most pole fires is calking around the gain with a suitable compound.

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2. Conductive Paint Prevents Wood Pin Burning, **C. C. Moler.** *Electrical World* (New York, N. Y.) April 1, 1944, page 1215.

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No Discussion

A-C Resistance of Large Size Conductors in Steel Pipe or Conduit

R. J. WISEMAN
FELLOW AIEE

Synopsis: The determination of the proper conductor size to carry a given power load on cables installed in steel conduits or pipes has been subject to assumptions which were not too satisfactory. The Insulated Power Cable Engineers Association engaged the Electrical Testing Laboratories to conduct a research project to determine the effective a-c resistance of large conductors installed in steel pipes. This paper reports the results of the research and gives suggested values of ratio of a-c to d-c resistance for conductor sizes up to 2,500,000 circular mils. It is felt that more accurate and uniform calculations of current carrying capacity of cables in conduit in air, or in pipe in the ground can be attained by use of the a-c/d-c conductor resistance ratios set forth herein, than by assumptions used previously, and therefore the data reported herein will be of benefit to both the manufacturer and the power user.

THE CURRENT carrying capacity of power cables installed in conduits or steel pipes is a function of the a-c resistance of the conductors, as well as the thermal resistance of the insulation and the allowable operating temperature. It is possible to compute the thermal resistance, and experience has set up the allowable operating temperature. Our knowledge of the a-c resistance up to the present has been very meager and not too reliable. In 1935, there was a paper presented before the AIEE, entitled Resistance and Reactance of 3-Conductor Cables,[1] which gave values of a-c resistance for 3-conductor cables in lead sheaths. The conductor sizes, however, were not over 1,000,000 circular mils, whereas much larger sizes are used in conduits. It was not certain that it would be possible to take the values reported in this paper and use them for conduits, as the steel binder used was thin, open spiralled, and paralleled the lead sheath, whereas in the case of a conduit we have a comparatively heavy wall and usually no paralleling grounding shield over the insulation.

Realizing the importance of knowing what this a-c resistance should be, the Insulated Power Cable Engineers Association sponsored a research to make tests on large size conductors when installed in conduits and to determine the increase in resistance over the d-c resistance when alternating currents were carried.

As this study was considered of major interest to all who use cables in pipes or conduits and particularly to cable manufacturers who are required to determine the proper cable size for a given power load to be carried, the Insulated Power Cable Engineers Association approached the cable manufacturers* who contributed to a fund for the investigation. In addition, the association itself appropriated an equal amount.

The research committee of the Association was delegated to supervise the research and was given permission to have the Electrical Testing Laboratories carry out the tests. A program was set up to guide Electrical Testing Laboratories in its work. A summary of the program is as follows.

Object of Research

The purpose of this investigation is to determine the a-c/d-c resistance ratio of large conductor cables when installed in steel pipe. To cover a wide range in conductor size, the cables to be tested will be: 750,000, 1,500,000, and 2,500,000 circular mils with standard concentric stranding; and 2,500,000 circular mils with segmental construction; all to have a 1/8-inch dry paper nsulation with a braid or tape covering. It was appreciated that there would be a lower ratio for cables with oiled strands, and therefore tests should be made to determine the ratio for such a condition, but funds being limited, it was decided to confine tests to dry strands. The length of conduit in each case is to be 20 feet.

In the setup for the tests of each cable, provision is to be made for circulating single-phase 60-cycle current; balanced 3-phase 60-cycle current; and direct current. The measurements to be made are to include d-c resistance, a-c resistance and reactance under the various conditions indicated in the following and to be made in each case over a range of currents up to well above the normal rating for the conductor size.

Tests on Data

The schedule of tests, each at not less than five values of current, is as follows:

1. All sizes, single conductor in air, with 48-inch loop, single-phase current.
2. All sizes, three bound together in triangular arrangement in air, 3-phase current.
3. Same as (2) with the 750,000-circular-mil conductors, in a 4-inch conduit; the 1,500,000-circular-mil conductors in a 5-inch conduit; and both types of 2,500,000-

Paper 48-254, recommended by the AIEE insulated conductor committee and approved by the AIEE technical program committee for presentation at the AIEE Midwest general meeting, Milwaukee, Wis., October 18-22, 1948. Manuscript submitted May 25, 1948; made available for printing August 17, 1948.

R. J. Wiseman is chairman of the Insulated Power Cable Engineers Association research committee and is associated with The Okonite Company, Passaic, N. J.

Personnel of the IPCEA committee on research: R. J. Wiseman, chairman, R. W. Atkinson, W. A. Del Mar and G. B. Shanklin.

* The wire and cable manufacturers who contributed to the funds for this research are: American Steel and Wire Company, General Electric Company, Habirshaw Cable and Wire Division (Phelps Dodge Copper Products Corporation), Kennecott Wire and Cable Company, The Kerite Company, National Electric Products Corporation, The Okonite Company, The Paranite Wire and Cable Corporation, Rockbestos Products Corporation, John A. Roebling's Sons Company, and the Simplex Wire and Cable Company.

The IPCEA committee wishes to express its appreciation to members of their companies' staffs for the help given in preparing the program and analysing the tests, particularly F. H. Buller, M. H. McGrath, L. Meyerhoff, J. H. Palmer, and R. C. Waldron.

Table I. Summary of Results as Read from Curves

Change of A-C Resistance with Temperature and with Operation Conditions of Spacing and Installation in Conduits

Test Number	Number of Phases	Operating Conditions: Spacing, Inches	Arrangement	Location	D-C Resistance at 20°C, Ohms Per 1,000 Feet	Average A-C Resistance Per Conductor, Ohms Per 1,000 Feet: 20°C	40°C	60°C	80°C	Ratio, A-C Resistance to D-C Resistance: 20°C	40°C	60°C	80°C
750,000-Circular-Mil Concentric-Stranded Cables													
1	1	48.0	Loop	Air	0.01395	0.0146	0.0157	0.01685	0.01795	1.045	1.040	1.037	1.032
2	3	1.32	Triangle	Air	0.01385	0.0160	0.01705	0.0181	0.0191	1.157	1.137	1.120	1.103
3	3	1.32	Triangle	4" conduit	0.01385	0.0181	0.0192	0.0203	0.02145	1.304	1.288	1.274	1.258
13	3	1.32	Triangle	5" conduit	0.01395	0.0176	0.01875	0.0199	0.0211	1.254	1.243	1.232	1.220
14	3	1.6	Triangle	Air	0.01405	0.01535	0.0165	0.0176	0.0187	1.095	1.092	1.088	1.084
19	3	1.6	Triangle	5" conduit	0.01395	0.01685	0.01785	0.0189	0.0199	1.208	1.190	1.172	1.154
15	3	1.6	Triangle	6" conduit	0.01385	0.01645	0.01755	0.0187	0.01985	1.183	1.175	1.167	1.157
20	3	1.6	Triangle	6" steel	0.01375	0.0171	0.0181	0.01905	0.0205	1.245	1.223	1.202	1.180
16	3	2.2	Triangle	Air	0.01397	0.01505	0.01615	0.01725	0.0183	1.073	1.068	1.063	1.057
17	3	2.2	Triangle	6" conduit	0.0138	0.01675	0.01775	0.0187	0.01865	1.210	1.188	1.167	1.147
18	3	2.2	Triangle	6" steel	0.01385	0.0176	0.0185	0.0194	0.0203	1.270	1.243	1.217	1.190
21	3	1.32	Flat	Air*	0.01403	0.0149	0.0163	0.01775	0.01915	1.067	1.077	1.087	1.097
22	3	1.32	Flat	Air**	0.01403	0.0155	0.0166	0.01765	0.01875	1.103	1.093	1.083	1.077
23	3	1.32	Flat	4" conduit	0.01395	0.0175	0.0186	0.0197	0.02075	1.256	1.237	1.219	1.202

* Cradled as in 4-inch electric conduit. ** Cradled as in 6-inch electric conduit.

circular-mil conductors in a 6-inch conduit.

4. Same as (2) with the 750,000-circular-mil conductors in a 5-inch conduit.

5. Same as (2) with the 750,000-circular-mil conductors spaced to give approximately the equivalent of 0.300-inch insulation on each conductor, in air, 3-phase current.

6. Same as (5) but in a 5-inch conduit.

7. Same as (5) but in a 6-inch conduit and also in a 6-inch steel pipe.

8. Same as (2) with the 750,000-circular-mil conductors spaced to give approximately the equivalent of 0.600-inch insulation on each conductor, in air, 3-phase current.

9. Same as (8) but in a 6-inch conduit and in a 6-inch steel pipe.

10. The three 750,000-circular-mil conductors in air but cradled to shape as if lying in a 4-inch conduit, 3-phase current.

11. Same as (10) but cradled to shape as if lying in a 6-inch conduit.

12. Same as (10) but in a 4-inch conduit.

13. The three 2,500,000 circular-mil concentric cables in air but cradled to shape as if lying in a 6-inch conduit, 3-phase current.

14. Same as (13) but in a 6-inch conduit.

Notes—In all tests in pipe, the 3-conductor cable formed by the triangular arrangement is to lie on the bottom of the pipe, as in actual installation.

Due to the size of the conductors and the short lengths for test, it was not possible to twist the conductors to form the usual arrangement of a 3-conductor cable.

During the period of testing, conferences were held to discuss results, and it became necessary to either modify tests or add new tests in order to obtain the desired results. Although we were interested in 3-phase testing of the cables in a conduit, to properly appreciate what is taking place, it was necessary to make single-phase and 3-phase tests on the cables in air prior to installing in the conduit. Many interesting problems arose, such as location of potential leads, time for holding the current at each value before taking final readings of conductor temperature, and external magnetic field effects arose which had to be solved.

This paper gives the results of the tests for a-c resistance obtained by Electrical Testing Laboratories, a discussion of them, and what conclusions can be made.

The method of testing, as well as a discussion of some of the problems which arose, will be found in a companion paper entitled Problems in the Measurement of A-C Resistance and Reactance of Large Conductors by E. H. Salter[2] of Electrical Testing Laboratories, who carried out the tests and prepared the official report for the Insulated Power Cable Engineers Association.

At the same time that the a-c resistance was being determined, it was possible to also measure the reactance. Wm. A. Del Mar, a member of the research committee, has a companion paper entitled Reactance of Large Cables in Steel Pipe or Conduit[3] in which he reports on this part of the research.

Currents were applied to give a curve of resistance versus conductor temperature up to about 80 degrees centigrade so that the effect of temperature on the a-c/d-c resistance ratio could be noted.

Results (In Air)

The general case is somewhat limited in its application and precision by the relatively meager extent of the data which could be secured within the bounds set by

Table II. Summary of Results as Read from Curves

Change of A-C Resistance with Temperature and with Operating Conditions of Spacing and Installation in Conduits

Test Number	Number of Phases	Operating Conditions: Spacing, Inches	Arrangement	Location	D-C Resistance at 20°C, Ohms Per 1,000 Feet	Average A-C Resistance Per Conductor, Ohms Per 1,000 Feet: 20°C	40°C	60°C	80°C	Ratio, A-C Resistance to D-C Resistance: 20°C	40°C	60°C	80°C
1,500,000-Circular-Mil Concentric-Stranded Cables													
4	1	48.0	Loop	Air	0.00690	0.00825	0.00875	0.00930	0.00980	1.19	1.172	1.158	1.150
5	3	1.70	Triangle	Air	0.00687	0.01050	0.01087	0.01132	0.01185	1.513	1.460	1.428	1.400
6	3	1.70	Triangle	5" conduit	0.00692	0.01303	0.01353	0.01400	0.01450	1.882	1.810	1.754	1.690
2,500,000-Circular-Mil Concentric-Stranded Cables													
7	1	48.0	Loop	Air	0.00400	0.00590	0.00610	0.00630	0.00647	1.48	1.415	1.355	1.305
8	3	2.17	Triangle	Air	0.00410	0.00790	0.00835	0.00870	0.00890	1.934	1.897	1.844	1.780
9	3	2.17	Triangle	6" conduit	0.00404	0.01074	0.01110	0.01143	0.01180	2.637	2.524	2.418	2.327
24	3	2.17	Flat	Air*	0.00403	0.00820	0.00870	0.00923	0.00978	2.026	1.994	1.963	1.933
25	3	2.17	Flat	6" conduit	0.00405	0.01205	0.01241	0.01279	0.01318	2.982	2.853	2.724	2.595
2,500,000-Circular-Mil Segmental-Sector Cables													
10	1	48.0	Loop	Air	0.00422	0.00473	0.00507	0.00540	0.00572	1.113	1.106	1.090	1.070
11	3	2.07	Triangle	Air	0.00420	0.00627	0.00657	0.00687	0.00717	1.493	1.437	1.393	1.356
12	3	2.07	Triangle	6" conduit	0.00430	0.00823	0.00853	0.00883	0.00913	1.932	1.857	1.791	1.742

* Cradled as in 6-in. electric conduit.

the funds available. However, the accuracy of the application is such that it is practical to obtain rather simply a useful solution for most conditions within the general scope of the project. Reasonable accuracy of estimate now can replace what were often merely controversial guesses. The data secured will greatly improve the accuracy of loss calculations and of current ratings. This should improve utilization of materials and, at the same time, help guard against dangerously high operating temperatures with resultant reduction of useful life.

Table I gives the data obtained for the 750,000-circular-mil conductor, and Table II for the 1,500,000-circular-mil and 2,500,000-circular-mil conductors.

Figure 1 shows the change in d-c and a-c resistance and ratio with temperature for the 1,500,000-circular-mil conductor in a 5-inch conduit. These curves are typical in form for all conductor sizes tested.

The measurements of a-c resistance for single-phase setup in air when compared to calculated values gave very close agreement which indicates that the test method used gives true values. For example: Table III compares the value of a-c and d-c resistance difference and ratio for the 1,500,000-circular-mil concentric stranded conductor as found by test and computed by means of the formula:

$$x = 0.02768 \sqrt{\frac{f}{R}}$$

where f is the frequency, R is the d-c resistance in ohms per 1,000 feet at the conductor temperature, and after calculating x, the skin effect factor may be found from tables such as that on page 3–23 of the Electrical Engineers Handbook by Pender and Del Mar.[4]

The foregoing formula for x can be converted over to one in terms of the conductor area and a temperature correction factor for resistance. If A is the area in circular inches and $b = 1 + 0.00393\ (t - 20)$ is the temperature correction factor referred to 20 degrees centigrade, then for $f = 60$ cycles,

$$x = 2.08\sqrt{A/b}$$

The same agreement has been found for other conductor sizes of concentric stranding.

Incidentally, the single-phase test on the 2,500,000-circular-mil segmental conductor gave a good chance to check the method for calculating the skin effect factor ratio of this design, namely, it shall be considered to be the same as that for a conductor having 44 per cent of the total area.

Table IV compares the skin effect factor for all sizes tested at 20 degrees centigrade. The actual ratios by test compare very well with calculated ratios. A study of the change in a-c resistance when making the 3-phase tests in air indicated an almost constant difference between the a-c and d-c resistance for a given conductor temperature. It was suggested by Louis Meyerhoff of the General Cable Corporation that it is possible to compute the a-c resistance for any temperature provided that the value for one temperature is known and if the a-c/d-c ratio is taken for that temperature, working backwards, determine an effective x, then correct this value of x for each temperature and find the new skin effect factor. Table V shows this comparison for the 1,500,000-circular-mil conductor spaced 1.70 inches in air. The values at 20 degrees centigrade were taken as reference points. The agreement at other temperatures may be considered as good and sufficiently accurate to use.

Table III. Comparison of A-C and D-C Resistance with Temperature

1,500,000 Circular Mils—Single-Phase Test—Spacing 48 Inches

Temperature, Degrees Centigrade	Computed Resistance, Ohms Per Thousand Feet A-C	Computed D-C	Computed Difference	Computed Ratio	By Test Resistance, Ohms Per Thousand Feet A-C	By Test D-C	By Test Difference	By Test Ratio
20	0.0084	0.0071	0.0013	1.19	0.0082	0.0069	0.0013	1.19
40	0.0089	0.0076	0.0013	1.17	0.0088	0.0075	0.0013	1.17
60	0.0095	0.0082	0.0013	1.16	0.0093	0.0080	0.0013	1.16
80	0.0099	0.0087	0.0012	1.14	0.0098	0.0085	0.0013	1.15

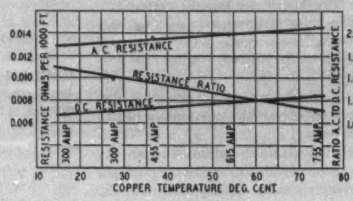

Figure 1. 1,500,000-circular-mil concentric strand cables, triangular arrangement, 1.70-inch spacing in 5-inch electric conduit

Table IV. Skin Effect Ratio at 20 Degrees Centigrade

Conductor Size, Thousand Circular Mils		D-C Resistance, Ohms Per Thousand Feet	A-C Resistance, Ohms Per Thousand Feet	Ratio, RA-C–RD-C Test	Ratio, RA-C–RD-C Calculated
750	Concentric	0.0140	0.0147	1.05	1.05
1,500	Concentric	0.0069	0.0082	1.19	1.19
2,500	Concentric	0.0040	0.0059	1.48	1.46
2,500	Segmental	0.0042	0.0047	1.12	1.11

Results (In Pipe)

The same reasoning can apply for the cables in a pipe as shown in Table VI for the same 1,500,000-circular-mil conductor. The difference between a-c and d-c resistances, computed and by test, is not great. The ratio comparison is fairly good and can be taken practically. Figure 1 shows the a-c and d-c resistance and the ratio of these resistances as a function of temperature.

The Meyerhoff method gives a workable means for determining the a-c re-

Table V. Comparison of A-C and D-C Resistance with Temperature as Computed and by Test for 3-Phase Setup

1,500,000 Circular Mils—1.70 Inch Spacing in Air

Temperature, Degrees Centigrade	Computed (Meyerhoff) Resistance, Ohms Per Thousand Feet A-C	Computed D-C	Computed Difference	Computed Ratio	By Test Resistance, Ohms Per Thousand Feet A-C	By Test D-C	By Test Difference	By Test Ratio
20	0.0105	0.0069	0.0036	1.52	0.0105	0.0069	0.0036	1.52
40	0.0110	0.0075	0.0035	1.46	0.0109	0.0074	0.0035	1.47
60	0.0115	0.0080	0.0035	1.43	0.0113	0.0080	0.0033	1.41
80	0.0118	0.0085	0.0033	1.39	0.0119	0.0085	0.0034	1.40

sistance, but it requires making measurements at one temperature for each conductor size, spacing, and size of pipe. As this is not practical, it is necessary to find a method which will give a curve showing the a-c/d-c ratio for each conductor size.

Where large conductors are close together in air, as when spaced by the insulation and coverings thereover, there is an increase in resistance over the d-c resistance due to:

1. The skin effect within the conductor.

2. The proximity effect which is the loss produced in one conductor by the current flowing in the adjacent conductor or conductors.

It is to be recognized that this distinction between the two sources of loss (1) and (2) is not precise, inasmuch as there is mutual interaction that prevents a precise application here of the principle of superposition. The separation is, however, very convenient and helpful in the case of the usual cable conductor. Where cables are placed inside a metallic pipe, there is an additional increase in a-c resistance over that when the cables are in air, this being due to the influence of the pipe upon the magnetic field produced in the conductors.

In the case of a ferrous pipe, there is caused a distortion and strengthening of the magnetic field which changes the proximity effect between the conductors. In this case, there are three sources of loss.

(1) Skin effect.

(2) Proximity effect due not merely to the current in the adjacent conductors but to the field distortion caused by the pipe.

(3) Loss in the pipe itself caused by hysteresis and eddy currents produced by the magnetic field.

Table VI shows also that the difference between the a-c and d-c resistance is nearly independent of temperature. Thus, for cables in pipe, as for cables in air, this simple fact may be used for obtaining the a-c resistance at different temperatures, or the somewhat more accurate Meyerhoff method may be used.

In order to determine how much of the added loss which is occasioned by the presence of the pipe is in the pipe and how much is due to added loss in the conductor, a special series of tests were made by calorimetric methods. The rate of rise of temperature of the pipe was determined for a given test condition and that was evaluated in terms of watts by determining the rate of rise of pipe temperature with known loss. These tests were made on the 2,500,000-circular-mil concentric stranded conductors. With these it was found that approximately 10 per cent of the total losses existed in the pipe. This is equivalent to 16 per cent of the extra (difference between a-c and d-c) losses. It is recognized that the particular division of losses is applicable only to this particular size and combination. However, it may be expected that using this percentage will give better temperature rise calculations than would the assumption that all losses are concentrated at the conductor.

Table VI. Comparison of A-C and D-C Resistance with Temperature as Computed and by Test for 3-Phase Setup

1,500,000 Circular Mils in 5-Inch Pipe—1.70-Inch Spacing

Temperature, Degrees Centigrade	Computed (Meyerhoff) Resistance, Ohms Per Thousand Feet A-C	D-C	Difference	Ratio	By Test Resistance, Ohms Per Thousand Feet A-C	D-C	Difference	Ratio
20	0.0131	0.0069	0.0062	1.90	0.0131	0.0069	0.0062	1.90
40	0.0138	0.0075	0.0063	1.84	0.0135	0.0075	0.0060	1.80
60	0.0142	0.0080	0.0062	1.78	0.0140	0.0080	0.0060	1.75
80	0.0148	0.0085	0.0063	1.74	0.0145	0.0086	0.0059	1.69

As a specific procedure, and in view of the recognition that the applicability of this test is limited without data on other sides, it has been proposed that, as a workable basis, 20 per cent of the extra losses be assumed to be in the pipe and the remaining 80 per cent in the conductor when the currents in the three conductors are balanced. This should be considered an acceptable basis within the limits of presently available information.

It has also been proposed that a single curve be used for the ratio Rac/Rdc as a simple function of the conductor size. This makes it independent of insulation thickness. It is also independent of pipe size on the assumption that each construction will be housed in the appropriate pipe. This suggestion has much to commend for its great simplicity and because it actually fits the test data so well. Detailed comparisons are shown later herein.

In order to find a closer correlation for specific insulation thicknesses and pipe sizes, R. W. Atkinson of the General Electric Company has proposed the use of certain empirical formulas. For the skin effect, the well-known method referred to previously could be used. The proximity effect would be determined as for the conductors in air; (See the Appendix for the Arnold method of computation); for the proximity effect in the pipe this value of proximity in air would be multiplied by F,

where

$$F=\frac{4.2\,S^2}{pd^2},$$

where S is the spacing of the conductors (center to center), p is the inside diameter of the pipe, and d the diameter of the conductor. The sum of the skin effect and proximity effect so determined would be considered as the added (a-c as compared with d-c) resistance within the conductors themselves. The increase in effective resistance due to loss within the pipe itself would be taken as

$$PL=\frac{0.00150S^2}{p}$$

ohms per thousand feet.

These formulas were worked out to express somewhat rationally the effect of the various parameters but essentially no claim is made for them, except that they may give a somewhat broader application to the data than can be done where the conductor separation and pipe size are not taken into direct account.

Table VII gives a comparison of com-

Table VII. Comparison of Computed and Electrical Testing Laboratories Values of R_{ac}/R_{dc} in Air and in Pipe at 20 Degrees Centigrade

Area, Thousand Circular Mils	Spacing, Inches	Pipe Size, (Inside Diameter) Inches	R_{ac}/R_{dc} In Air Computed	ETL	In Pipe Computed	ETL
750	1.32	4.06	1.14	1.16	1.30	1.30
	1.32	5.03			1.25	1.25
	1.60	5.03	1.11	1.10	1.29	1.21
	1.60	6.16 conduit			1.25	1.18
	1.60	6.06 steel			1.26	1.25
	2.20	6.16 conduit	1.08	1.07	1.35	1.21
	2.20	6.06 steel			1.36	1.27
1,500	1.70	5.03	1.46	1.51	1.88	1.88
2,500 concentric	2.17	6.16 conduit	1.92	1.93	2.72	2.64
2,500 segmental	2.07	6.16 conduit			1.49	1.88	1.93

Table VIII. Comparison of R_{ac}/R_{dc} at 70 Degrees Centigrade for Total Losses—in Pipe

Conductor Size, Thousand Circular Mils	Proposed From ETL Tests	Computed Average
250	1.06	1.04
500	1.14	1.14
750	1.27	1.26
1,000	1.41	1.41
1,250	1.57	1.57
1,500	1.72	1.76
1,750	1.89	1.90
2,000	2.05	2.10
2,500	2.37	2.40

Table IX. Comparison of R_{ac}/R_{dc} at 70 Degrees Centigrade

Losses at Conductors

Conductor Size Thousand Circular Mils	Proposed From ETL Tests	Computed Average
250	1.05	1.03
500	1.11	1.10
750	1.22	1.19
1,000	1.33	1.32
1,250	1.46	1.45
1,500	1.57	1.59
1,750	1.70	1.70
2,000	1.84	1.86
2,500	2.10	2.09

puted values on the foregoing basis for the conductor sizes, spacings, and pipe sizes used by Electrical Testing Laboratories and the ETL values.

The agreement between computed and test values is probably as good as could be expected in view of the fact that there is no mathematical analysis available to determine the increase in resistance except to some degree for the cables in air and empirical formulas have been set up for the effect of magnetic pipe.

In view of the fair agreement between the Electrical Testing Laboratories and computed values of R_{ac}/R_{dc} as indicated in Table VII, calculations at 70 degrees centigrade were made for a large number of conductor sizes and insulation thicknesses from 1/8 inch to 1 inch. A pipe diameter was taken such that the area of the insulated conductors was equal to 40 per cent of the area of the pipe. It was necessary to take the exact pipe diameter in order to be able to get a curve. If this were not done, the ratios spread out badly. In actual practice, the nearest standard pipe would be used, accepting whatever error in the ratio which would exist. This error is small as it affects the proximity and pipe loss terms which are two terms of a total of four terms, one of which is the d-c resistance which is much the largest of all. The ratio R_{ac}/R_{dc} did not vary greatly. For a 250,000-circular-mil conductor from 1.04 to 1.05; a 750,000-circular-mil 1.24 to 1.27; a 1,250,000-circular-mil 1.54 to 1.61; a 2,000,000-circular-mil 2.10 to 2.14. If the five values obtained (for t = 1/8 inch, 1/4 inch, 1/2 inch, 3/4 inch and 1.0 inch) were averaged, the variations therefrom are not great.

Table VIII gives the average ratio of R_{ac}/R_{dc} for total losses at 70 degrees centrigrade and are taken from a curve plotted with the Electrical Testing Laboratories test values. In comparison, the last column gives the computed average values as obtained by means of the empirical formulas. It is in order to use the column giving the Electrical Testing Laboratories values, but if one wishes, he could use the computed values within the accuracy of the test results.

Table IX gives a comparison of the ratio of R_{ac}/R_{dc} at the conductor based on the suggestion of G. B. Shanklin of the General Electric Company that 80 per cent of the extra losses be assumed to exist in the conductor and based on the empirical formula. The agreement between the proposed values and the computed average values is very good. Either could be used but to be consistent, if the Electrical Testing Laboratories values for the ratio of R_{ac}/R_{dc} for total losses as given in Table VIII are used, then the proposed ratios for the losses at the conductor should be used.

Up to the present, concentric-stranded conductors have been discussed. Tests were also made with a 2,500,000-circular-mil segmental conductor cable.

A definite decrease in the ratio R_{ac}/R_{dc} was noted as compared to a concentric-stranded conductor of same area as shown in Table VII. When the construction of the segmental conductor is considered, this is not surprising. It is known that the skin effect is much less. Due to the spirality of the segments, it is fair to state that the proximity effect is less. Therefore, the skin effect plus proximity effect in air should be less. This is shown to be true by the tests in air of both conductor constructions, namely for 20 degrees centigrade, R_{ac}/R_{dc} = 1.93 for concentric stranding and 1.49 for segmental construction. (See Table II.) When the cables are in a pipe, it is not certain that there will be the same amount of pipe loss. It cannot be greater than for a concentric-stranded conductor, but it can be less. If it is assumed to be the same, then it is possible to use the same formula for computing the increase in resistance due to pipe loss (PL = 0.00150 S^2/p ohms per thousand feet) and then correct the factor by which the proximity of a concentric-stranded conductor is multiplied to get the assumed proximity loss due to the pipe. This was done and it was found that if the constant 4.2 in the formula were changed to 2.4, that is,

$$F = \frac{2.4S^2}{pd^2}$$

a value of proximity was obtained which gave a reasonable close value of total loss to that found by Electrical Testing Laboratories: namely at 70 degrees centigrade R_{ac}/R_{dc} = 1.74 by computation and 1.76 by Electrical Testing Laboratories. It is admitted that considerable liberty has been taken with assumptions. It would have been much better, if it were possible, to have tests made on other sizes of segmental conductors. In view of very limited data (only one conductor size studied), it is felt that at the present time we postpone setting up a tabulation of R_{ac}/R_{dc} for segmental conductors as for concentric conductors, as given in Tables VIII and IX. Some manufacturers are now making tests with several conductor sizes and will give the results to the Association when completed.

As originally planned, all tests were made with the three conductors tied together in triangular arrangement. Question arose as to what the effect of the cables lying in a flat arrangement would have on the a-c resistance. Tests were made on the 750,000-circular-mil conductor and the 2,500,000-circular-mil concentric-stranded conductor. These are reported in the Electrical Testing Laboratories report and given in Tables I and II.

Table X gives a comparison of the ratio R_{ac}/R_{dc} for the 750,000-circular-mil conductor for triangular and flat arrangements in air and in pipe. The cables with flat arrangement in air were cradled to give the shape that they would take when lying in a 4-inch conduit.

Table X. Comparison of R_{ac}/R_{dc} for 750,000-Circular-Mil Conductor When Arranged in Triangle and Flat

	Temperature, Degrees Centigrade 20	40	60	80
Triangle air	1.157	1.137	1.120	1.103
Flat air	1.067	1.077	1.087	1.097
Triangle pipe (4 inch)	1.304	1.288	1.274	1.258
Flat pipe (4 inch)	1.256	1.237	1.219	1.202

Placing the conductors in a flat arrangement reduces the ratio R_{ac}/R_{dc} about 5 per cent. This is surprising, as the magnetic field is more unbalanced and a greater amount of flux will exist in the pipe; therefore, we would expect a higher loss. It would appear that the proximity effect is decreased. This is indicated in the re-

Table XI. Comparison of R_{ac}/R_{dc} for 2,500,000-Circular-Mil Concentric Stranded Conductor Arranged in Triangle and Flat

	Temperature, Degrees Centigrade 20	40	60	80
Triangle air	1.934	1.897	1.844	1.780
Flat air	2.026	1.994	1.963	1.933
Triangle pipe, 6 inch	2.637	2.524	2.418	2.327
Flat pipe, 6 inch	2.982	2.853	2.724	2.595

sults for the tests in air where a lower ratio was also found.

Table XI gives a comparison of the ratio R_{ac}/R_{dc} for the 2,500,000-circular-mil concentric-stranded conductor in triangular and flat arrangement, and in air and in a 6-inch pipe.

In this case, there is an increase in the ratio for both in air and in pipe, about 5 and 13 per cent respectively. This is directly opposite to the results obtained for the 750,000-circular-mil conductor. It is difficult to understand why such diametrically opposite results were obtained. It would seem as if the results for the 2,500,000-circular-mil conductor were more logical, yet should the results for the 750,000-circular-mil conductor be ignored? It would be more conservative to assume that there is an increase in the ratio and if a 15 per cent increase is taken, for a flat or random arrangement, as compared to a triangular arrangement, it should definitely cover the actual increase. This can only be done for the ratios taken from curves for total losses. Here also we are taking liberties with assumptions and perhaps further tests will help to establish a more accurate increase in ratio. It may be lower.

Table XII has been computed on the basis of the increase in total losses of 15 per cent for flat or random arrangement as set up in Table VIII for concentric standard stranding.

No attempt has been made to set up ratios for segmental conductors in flat or random arrangement.

The increase in a-c resistance due to random arrangement of conductors in a pipe over the a-c resistance when placed in triangular arrangement indicates that, if feasible, the cables when being drawn into a pipe should be tied together in triangular arrangement, particularly if the length of pull is short, that is, only a few hundred feet. The chance of cables assuming a flat or random arrangement is not too great. The pipe size selected is never large enough to allow a flat arrangement in order to prevent jamming of the cables during the pulling in operation.

Table XII. Assumed Values of R_{ac}/R_{dc} for Flat Arrangement for Total Losses

Conductor Size Thousand Circular Mils	Total Losses
250	1.22
500	1.31
750	1.46
1,000	1.62
1,250	1.80
1,500	1.98
1,750	2.17
2,000	2.36
2,500	2.73

Table XIII. Results of Measurement of Resistivities and of Magnetic Performance of Conduit Used in Cable Investigation

	4-Inch Conduit	5-Inch Conduit	6-Inch Conduit	6-Inch Steel Pipe
Resistivity, microhms (inches)				
(a) at 20 degrees centigrade	5.31	4.86	5.025	6.25
(b) at 40 degrees centigrade	5.75	5.30	5.47	6.685
Resistance-temperature coefficient, per cent per degree centigrade from and at 20 degrees centigrade	0.41	0.45	0.44	0.35
Induction, gausses (B)				
$H=1$	650	550	600	350
$H=2$	2,700	2,100	2,550	1,100
$H=5$	8,100	8,250	8,500	6,700
$H=10$	11,600	11,800	11,800	10,600
Residual	6,350	7,200	6,300	6,800
Coercive force, Gilberts per centimeter	2.1	2.7	1.95	2.7

Resistivity and Magnetic Properties of the Conduits

As a part of the investigation to determine the amount of extra a-c losses residing in the conduit, it was necessary to measure the d-c resistance of the 6-inch conduit and to make the report complete, all conduits were measured for d-c resistance. Also, the magnetic properties of each size were measured as a matter of record. Table XIII gives the results of both sets of measurements. There is no close agreement for the three sizes of conduit for resistivity, and the 6-inch steel pipe seems to have a higher resistivity than the conduits. There is not a great deal of difference in the magnetic properties of the conduits and steel pipe. What variations in the resistivity and magnetic properties occur are not important as the losses in the pipe and conduit only amount to 10 per cent of the total losses, and a variation of only 2 or 3 per cent in total losses.

To complete our records on the characteristics of the materials used, the American Steel and Wire Company and John A. Roebling's Sons Company, through the courtesy of J. J. Morrison and A. P. Bellis respectively, determined the chemical composition of each of the pipes used, as shown in Table XIV.

The chemical composition of the conduit does not differ greatly except that the phosphorous content of the 4-inch conduit is quite high, whereas for the other conduits it is low. In the case of the steel pipe, the copper and silicon contents are about ten times their respective values for conduits.

This research has provided means for calculating the effective a-c resistance of conductors in magnetic pipes (Tables VIII, IX, and X). Assumptions have been made regarding the amount of loss in the pipe so that it is possible to assign to the conductor an effective a-c resistance which, when multiplied by the square of the current, gives a loss at the conductor to which can be multiplied the thermal resistance of the cables inside the pipe to give the temperature drop between the conductors and the pipe. Multiplying the total effective a-c resistance by the square of the current gives the total loss which, if multiplied by the surface thermal resistance of the conduit, will give the

Table XIV. Chemical Composition of Steel Pipes

Diameter of Pipe	Conduits 4 Inches A	4 Inches R	5 Inches A	5 Inches R	6 Inches A	6 Inches R	Steel 6 Inches A	Steel 6 Inches R
Carbon	0.06	0.054	0.12	0.117	0.09	0.083	0.08	0.08
Manganese	0.41	0.40	0.45	0.435	0.49	0.49	0.45	0.44
Phosphorus	0.112	0.107	0.008	0.011	0.012	0.012	0.093	0.086
Sulphur	0.048	0.043	0.062	0.065	0.046	0.050	0.032	0.032
Silicon	0.005	Trace	0.007	Trace	0.005	Trace	0.055	0.062
Copper	0.03	0.035	0.03	0.037	0.03	0.023	0.31	0.274
Nickel	0.01		0.01		0.01		0.01	
Chromium	0.02		0.02		0.02		0.02	
Tin	0.005		0.005		0.005		0.005	

A—American Steel and Wire Company tests. R—John A. Roebling's Sons Company Tests.

temperature drop of the conduit. In the case of a buried pipe, this total loss, multiplied by the thermal resistance of the earth, will give the temperature drop of the pipe to earth.

It is recognized that liberties have been taken in making assumptions. It is felt, however, that they are not so broad as to cause serious error in the desired result, namely, a method for determining the total power loss due to current flowing in conductors installed in steel pipes. It is hoped that anyone who is investigating this subject will furnish any data that they have obtained to the committee and perhaps later it may be possible to revise our recommendations.

Appendix. Empirical Method for Calculating the Apparent A-C Resistance of Three Single Conductor Cables in a Steel Pipe

The following empirical method for calculating the apparent a-c resistance of three single-conductor cables in a steel pipe consists (a) in determining the skin effect and proximity effect for the conductors in air by means of the Arnold formula; (b) compute the skin effect, also by means of the Arnold formula for skin effect; (c) subtract (b) from (a), which gives the proximity effect in air; (d) calculate a factor which is a function of conductor diameter, spacing of conductors, and inside diameter of pipe; (e) multiply this factor by (c) to get the proximity of the cable in the pipe. If the skin effect, the proximity effect of the cable in the pipe, and the d-c resistance are added together, we get the total apparent a-c resistance of the conductors. The loss in the pipe is also determined by an empirical formula and is expressed as an increase in resistance. If this latter resistance is added to the skin effect and the proximity effect of the cables in the pipe, the total effective increase in resistance assumed as manifested at the pipe is obtained, which, when added to the d-c resistance, gives the apparent effective a-c resistance external to the pipe.

Table XV

Values of $f(x)$, $a(x)$, $b(x)$, and $c(x)$ for Various Values of x

x	$f(x)$	$a(x)$	$b(x)$	$c(x)$
0	1.0	0	0	0.090
0.25	1.0	0	0	0.090
0.50	1.0	0	0	0.090
0.75	1.0	0.013	0	0.082
1.00	1.005	0.030	0	0.060
1.25	1.014	0.080	0	0.009
1.50	1.028	0.136	0	0.008
1.75	1.047	0.222	0.008	0.042
2.00	1.080	0.322	0.024	0.072
2.25	1.109	0.415	0.040	0.080
2.50	1.175	0.500	0.055	0.085
2.75	1.240	0.568	0.067	0.112
3.00	1.318	0.614	0.070	0.150
3.25	1.403	0.644	0.067	0.200
3.50	1.475	0.668	0.054	0.266
3.75	1.586	0.684	0.043	0.334
4.00	1.683	0.696	0.040	0.411

It is to be noted that this method is entirely empirical and that the constants were so chosen as to tend to follow the trend indicated by the few measurements of the Electrical Testing Laboratories series that were made with varying spacings between conductors or varying pipe size.

The calculation of skin effect and proximity effect of two insulated conductors spaced a distance S apart in air may be found by means of formulas developed by Doctor A. H. M. Arnold.[5] He uses in his formula a constant k as a correction factor for the stranding of a conductor. This factor may vary with degree of stranding and whether strands are clean, oxidized, dry or oiled, and ranges from $k = 0.45$ to 0.90. In this method $k = 1$ is taken.

Skin Effect

If

R = d-c resistance of conductor in ohms per kilometer
R' = a-c resistance of conductor in ohms per kilometer
f = frequency of alternating current in cycles per second
R'/R = skin effect ratio

Then

$$x = 0.01\sqrt{\frac{8\pi f}{R}} \tag{1}$$

$$R'/R = f(x)$$

which is a function of x as given in Table XV in column marked $f(x)$

If

$f = 60$ cycles and R_{dc} is the resistance per thousand feet at the desired temperature of conductor, then

$$x = 0.215/\sqrt{R_{dc}}$$

Skin Effect and Proximity

If

R'' = a-c resistance due to both skin effect and proximity effect for the cables in air
$\alpha = d/S$ = ratio of diameter of conductor to spacing (center to center) between conductors
$a(x)$, $b(x)$ and $c(x)$ are Bessel functions of x,

Then

$$R''/R = \frac{f(x)}{\sqrt{1-\alpha^2 a(x) + \frac{\alpha^4 b(x)}{1-\alpha^2 c(x)}}} \tag{2}$$

$a(x)$, $b(x)$ and $c(x)$ may be found for various values of x up to 4.0 (in Table XV).

Calculate the actual value of skin effect (ohms per thousand feet) by means of equation (1) and skin effect and proximity effect (ohms per thousand feet) by means of equation (2) and then subtracting equation 1 from equation 2 we get the proximity effect (ohms per thousand feet) alone. Call it

$$P_a \tag{3}$$

Proximity effect due to pipe (concentric stranding)

S = spacing (center to center) between conductors, inches
p = inside diameter of pipe, inches
d = diameter of conductor, inches
F = factor to multiply proximity effect in air by to get proximity effect in pipe

$$F = \frac{4.2\,S^3}{pd^2}$$

Then

P_p = increase in resistance due to proximity effect with cables in pipe in ohms per thousand feet

$$P_p = FP_a \tag{4}$$

Increase in resistance due to pipe in ohms per thousand feet

$$PL = 0.00150\frac{S^3}{p} \tag{5}$$

Total effective a-c resistance at conductor in ohms per thousand feet

$$R_{acc} = R_{dc} + \text{skin effect} + FP_a$$

Total effective a-c resistance at pipe in ohms per thousand feet

$$R_{acp} = R_{acc} + PL$$

To use these resistances in order to determine the allowable current in a cable, multiply the total thermal resistance per foot of the cable inside the conduit or pipe by R_{acc} and the external thermal resistance per foot of the conduit or pipe by R_{acp}. Take the sum of both products and divide the allowable temperature rise of the conductor by this summation. Take the square root of the quotient, and this will be the allowable current. The internal and external thermal resistances should be expressed in terms of a single cable.

Example on How to Use Formulas

Calculate the apparent a-c resistance of a 1,000,000-circular-mil conductor having an 0.250-inch wall of insulation with a covering (tape or braid or neoprene jacket) which gives an added thickness of 0.065-inch; (a) at the conductor and (b) at the pipe.

Assume standard concentric stranding

$d = 1.152$ inches = conductor diameter
$2t = 0.630$
$S = 1.782$ inches = spacing of conductors center to center

If it is assumed that the cables occupy 40 per cent of the inside area of the pipe, then the inside diameter of the pipe $p = S\sqrt{7.5} = 2.74S$ inches. Actually the next larger standard pipe size would be used. In determining the calculated ratios given in the report, the actual diameter based on the 40 per cent area was used, otherwise the values of R_{ac}/R_{dc} for various conductor sizes would not lie on a curve.

$p = 2.74 \times 1.782 = 4.88$,
$\alpha = d/S = 1.152/1.782 = 0.646$
$\alpha^2 = 0.418$; $\alpha^4 = 0.175$
$S = 1.782$; $S^3 = 3.18$; $S^2 = 5.67$
$d = 1.152$; $d^2 = 1.328$
$R_{dc} = 0.0108$ ohm per thousand feet at 25 degrees centigrade

Assume conductor temperature to be 70 degrees centigrade

$$\Delta R = 0.00385 \times 45 \times 0.0108 = 0.00187$$

$$R_{dcm} = 0.0108,$$

$$R_{dcu} = 0.01267$$

$$\sqrt{R_{dcu}} = \sqrt{0.01267} = 0.1125, \text{ ohms per thousand feet}$$

From Table XV

$f(x) = 1.065 = R'/R =$ Skin effect ratio

$a(x) = 0.285;\ b(x) = 0.019;\ c(x) = 0.065$

$$\alpha^2 c(x) = 0.418 \times 0.065 = 0.027$$

$$1 - \alpha^2 c(x) = 0.973$$

$$\alpha^4 b(x) = 0.175 \times 0.019 = 0.0033$$

$$\frac{\alpha^4 b(x)}{1 - \alpha^2 c(x)} = \frac{0.0033}{0.973} = 0.003$$

$$\alpha^2 a(x) = 0.418 \times 0.285 = 0.119,$$

$$1 - \alpha^2 a(x) + \frac{\alpha^4 b(x)}{1 - \alpha^2 c(x)} = 1 - 0.119 + 0.003$$

$$= 0.884$$

$$\sqrt{0.884} = 0.940$$

$$R''/R = 1.065/0.940 = 1.130$$

(R''/R is ratio for skin effect plus proximity in air.)

$$R'' - R = 0.130 \times 0.01267 = 0.00165$$

= skin effect + proximity in air

$$R' - R = 0.065 \times 0.01267 = 0.00082$$

= skin effect, ohms per thousand feet

$$P_a = 0.00083$$

= proximity in air, ohms per thousand feet

$$P = \frac{4.2S^3}{pd^2} = \frac{4.2 \times 5.67}{4.88 \times 1.328} = 3.68,$$

$$P_p = F \times P_a = 0.00083 \times 3.68 = 0.00305 \text{ ohms per thousand feet}$$

$$PL = 0.00150 \frac{S^3}{p} = \frac{0.00150 \times 3.18}{4.88} = 0.00098$$

ohms per thousand feet

$$R_{dc} = 0.01267$$

Skin effect $= 0.00082$

$$P_p = 0.00305$$

$R_{acc} = 0.01654$ ohms per thousand feet

$$PL = 0.00098$$

$R_{acp} = 0.01752$ ohms per thousand feet

$$R_{acc}/R_{dc} = \frac{0.01654}{0.01267} = 1.31$$

$$R_{acp}/R_{dc} = \frac{0.01752}{0.01267} = 1.38$$

The tabulation in text gives 1.32 and 1.41 respectively.

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Discussion

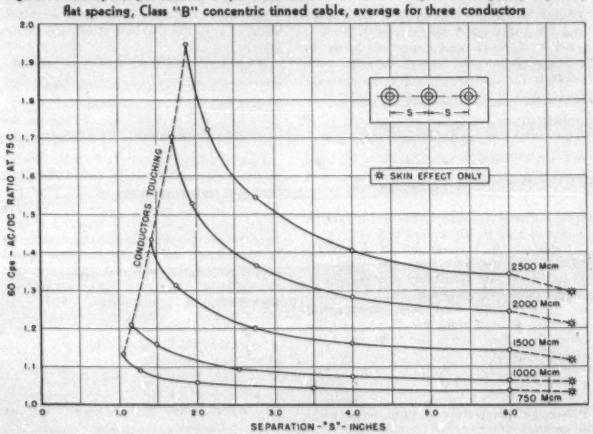
L. F. Roehmann (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): The discussion of Wiseman's paper will be confined to concentric-stranded cables. This work can be divided into three groups:

1. Single-phase loops in air.
2. Three conductors 3-phase, triangular or flat, in air.
3. Three conductors 3-phase, triangular or flat, in magnetizable conduit.

Group 1 is, of course, easily amenable to calculation and, as expected, measured and calculated values check very well.

Group 2 is amenable to calculation also, but such calculations have been omitted. Figure 1 of the discussion shows calculated a-c/d-c resistance ratios for three single conductors in air, sizes 750,000 to 2,500,000 circular mils, energized with 3-phase current 60 cycles per second, 75 degrees centigrade conductor temperature, flat arrangement, as a function of spacing. These data were calculated by A. C. Connell of our organization from Arnold's formulas. If during the tests in air the three conductors had been arranged flat (instead of cradled), and if the spacing had been varied systematically a direct check would be possible. Even now, it is still possible to calculate from the Arnold formulas the a-c/d-c resistance ratios for the triangular arrangements which were tested in air, and to compare them with the measured values.

Figure 1. 60 cycles per second a-c/d-c resistance ratio at 75 degrees centigrade, 3-phase, flat spacing, Class "B" concentric tinned cable, average for three conductors



Group 3 due to its complexity seems to defy calculation at first sight. However, Atkinson and Wiseman have developed empirical formulas which are quite sim le and which permit calculation of conductor plus pipe losses with an accuracy which is sufficient for practical purposes. This is, indeed, quite an achievement, and the authors should be congratulated.

The question which the discusser asks, without answering it, is this: will the formulas given by Arnold in 1941[1] permit a calculation of this group also? In other words: was this new effort justified, or could Arnold's semiempirical formulas (for the "armouring effect in cables with magnetic armouring") have accomplished the same end? Arnold's formulas show an amazingly high degree of accuracy compared with measurements, usually within one per cent, but his formulas are rather cumbersome. The Atkinson-Wiseman corrections are extraordinarily simple but less accurate. How will Arnold's formulas check the Electrical Testing Laboratories findings for three cables in magnetizable conduit?

Reference

1. See reference 5 of the paper.

L. Brieger (Consolidated Edison Company of New York Inc., New York, N. Y.): For the calculation of 3-phase resistance of cables in air, Wiseman uses Arnold's single-phase formula,[1] even though Arnold also published two formulas for the 3-phase system. One of these is to be found in the paper Eddy Current Losses in Multi-core

Table I

Cable Size, Thousand Circular Mils	Spacing, Inches	At 20 Degrees Centigrade $x = 0.215$ $\alpha = d/D$	$\sqrt{R_{dc}}$	μ	Formula (a) p_s	p_p	$\frac{R_{ac}}{R_{dc}}$	Formula (b) P_1	q_t	$\frac{R_{ac}}{R_{dc}}$	Curtis' Formula $\frac{R_{ac}}{R_{dc}}$ Single Phase
750	1.324	0.752	1.82	0.14	1.054	0.117	1.17	1.133	1.031	1.17	1.12
750	1.598	0.625	1.82	0.10	1.054	0.079	1.13	1.107	1.022	1.13	1.10
750	2.198	0.453	1.82	0.05	1.054	0.041	1.1	1.08	1.014	1.1	1.07
1,500	1.7	0.83	2.59	0.35	1.198	0.358	1.55	1.456	1.065	1.55	1.46
2,500	2.17	0.84	3.38	0.48	1.449	0.588	2.03	1.919	1.072	2.05	1.93

Paper Insulated, Lead Covered Cables, Armored and Unarmored, Carrying Balanced 3-Phase Current.[3] The second formula of Arnold's will be seen in the paper, Proximity Effect in Solid and Hollow-Round Conductors.[3] The working formulas for the 3-phase system will be found on page 354 of the last mentioned publication, the resistance ratio for three phase being given as the product of two factors. The first is the single-phase proximity factor which is the one which Wiseman uses and the second factor is a multiplier for three phase. Arnold gives factors for triangular as well as flat spacing.

Arnold's 3-Phase Formulas for AC/DC Resistance Ratio

Formula (a),[3] page 55

$$\frac{R_{ac}}{R_{dc}} = p_s + p_p \quad p_s = \text{skin effect factor} = f(x)$$

$$p_p = 3\,\phi \text{ proximity effect factor}$$

$$= \frac{3}{2}\,\alpha^2 G(x)/\{1 - 5/24\alpha^2 H(x)\}$$

$$\alpha = d/D$$

the functions f, G and H are tabulated by Arnold.

Formula (b): $\frac{R_{ac}}{R_{dc}} = q p_1$,

where

p_1 = single-phase factor including skin and proximity effects.

$$= \sqrt{\frac{f(x)}{1 - \alpha A^2(x) + \frac{\alpha B^4(x)}{1 - \alpha C^2(x)}}}$$

q_t (for triangular spacing)

$$= 1 + \frac{1}{4}\mu \frac{5}{24}\mu^2 - \frac{3}{8}\mu^3$$

q_f (for flat spacing) $= 1 + \frac{1}{4}\mu - \frac{1}{8}\mu^2 + \frac{5}{24}\mu^3$

$\mu = \alpha^2 A(x)$; the other symbols are the same as Wiseman's.

These formulas assume that the interstrand resistance correction factor $K = 1.0$, which represents perfect contact between strands.

The effect of the multiplying factor q is significant for large cables at close spacing as indicated by Table I of the discussion.

Wiseman reports good agreement between the test results and Arnold's single-phase formula. But as the 3-phase formulas, would have given higher values, it may be inferred that the correction factor K, which Arnold uses to take into consideration, the effect of interstrand

Table II

Cable Size, Thousand Circular Mils	Spacing, Inches	20°C	70°C
750	1.324	0.93	0.93
1,500	1.7	0.85	0.9
2,500	2.17	0.72	0.81

resistance of the cables upon the proximity effect and which Wiseman assumes to be 1.0, should actually be taken as less than unity. If we assume that Arnold's 3-phase formulas are basically correct and that differences between tests and calculations may be viewed as due to contact resistance between strands, the following values of K would bring the tests and calculations into numerical agreement, as shown in Table II of the discussion.

On the subject of interstrand resistance of dry and impregnated cables clarification is necessary. In the paper presented by Salter, Shanklin and Wiseman,[4] it is implied that impregnation reduces the proximity effect, hence the interstrand resistance is larger for impregnated cables. Arnold on the other hand reports that the contact resistance between strands is greater for dry than for impregnated cables and that even for impregnated cables it does not exceed 0.8. Wiseman's committee would do a good service by clearing up this contradiction.

Before leaving the subject of theoretical formulas, I wish to draw attention to a paper written by H. L. Curtis, in 1920.[5] Curtis derived a power series expression for the ac/dc resistance of a single-phase circuit. This series is laborious to evaluate, but he has checked his method by tests on number 2 copper wires at various spacings and at frequencies up to 3,000 cycles. Curtis' formula agrees with Arnold's, and with all others which have been developed, in the substantial principle that the resistance ratio in air is a function of only two parameters: (1) the ratio of frequency and d-c resistance per unit length (f/R_{dc}), and (2) the ratio of conductor diameter to spacing between centers (d/D).

Curtis' data can, therefore, be readily recalculated for all practical conductor sizes at 60 cycles. This was done, and the data are plotted on Figure 2 of the discussion. The agreement with Arnold's single-phase formula may be judged by reference to Table I of the discussion. It would be simple to plot curves for 3-phase flat or triangular arrangement by using Arnold's multipliers, thus obviating the need for calculations so long as a single frequency is involved. More will be said about Curtis' studies in connection with reactance.

Table III

Arrangement	Conduit	R_{ac} at 25 Degrees Centigrade, Ohms Per 1,000 Feet	R_{ac}/R_{dc}
Triangular	4-Inch fibre	0.0153	1.095
Triangular	4-Inch iron	0.0162	1.16
Flat	5-Inch fibre	0.0154	1.10
Flat	5-Inch iron	0.0168	1.20

In addition to the foregoing remarks as to procedure, a few comments are submitted on the numerical data in the paper.

The comparative resistance ratios for 750,000 and 2,500,000-circular-mil concentric-strand conductors in triangular and flat configurations require critical review. I share Wiseman's suspicions regarding the data on 750,000-circular-mil cables. As a substitute, results of tests made on 750,000-circular-mil rubber-insulated low-voltage cables ($D = 1.475$ inch spacing) by the Consolidated Edison Company of New York, will be seen in Table III of the discussion.

The a-c resistance in air is practically the same for triangular and for flat arrangement which is consistent with Arnold's theoretical data.

The Electrical Testing Laboratories' tests on 2,500,000-circular-mil cables look better, although the values are not quite in agreement with theory. As Arnold reported good agreement between tests and calculations for flat arrangement, his 3-phase multipliers may be accepted as a basis of discussion. Table IV of the discussion is instructing because it shows the range of changes which may be expected theoretically.

Table IV

μ	q_t	q_f	q_f/q_t
0.2	1.042	1.045	1.0
0.4	1.066	1.081	1.014
0.5	1.072	1.095	1.023
0.6	1.019	1.100	1.037
0.8	1.004	1.155	1.155
1.0	0.667	1.334	2.0

It is interesting that the multiplier for triangular disposition reaches a maximum and ultimately becomes less than 1.0 which means that for large conductors very close together, the 3-phase resistance may be smaller than the single-phase resistance. The ratio of flat and triangular arrangement increases continuously with increasing cable size and decreasing spacing, although the increase is rather slow until large conductor sizes are reached. For the 2,500,000-circular-mil cables on the Electrical Testing Laboratories' test $\mu = 0.48$ (see Table I of the discussion) and the flat arrangement offers about 2.3 per cent greater resistance than the triangular assembly. Wiseman's reports the increase as 4.6 per cent at 20 degrees centigrade and 8.5 per cent at 80 degrees centigrade. If theory means anything, the ratio between flat and triangular disposition should decrease with temperature.

In reference to Table VIII of the paper which contains the ac/dc resistance ratios

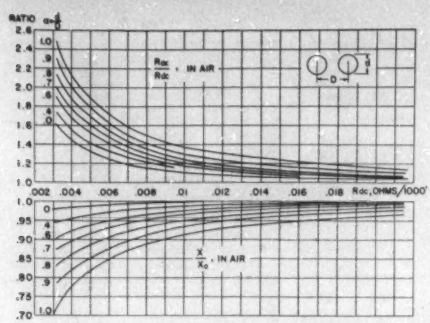


Figure 2. Chart based on Curtis' formulas

for cables in pipe, tests made by Consolidated Edison Company on rubber-insulated low-voltage cables are submitted for comparison with the data in the table for the first three conductor sizes listed. The tested R_{ac}/R_{dc} at 25 degrees centigrade were as shown in Table V of the discussion.

Table V

4/0 triangular—1.03 in 3-inch iron pipe
500,000-circular-mil triangular—1.075 in 3-inch and 4-inch iron pipe
750,000-circular-mil triangular—1.13 in 4-inch iron pipe

These figures are lower than Wiseman's in spite of the low temperatures of the Consolidated Edison Company test. Perhaps interstrand contact resistance is raising its ugly head.

The 15 per cent over-all increase for flat or random disposition in pipes over triangular arrangement in pipes may be satisfactory for large cables, but it is too much for the smaller conductors. This is indicated by the comparison in Table VI of the discussion between laboratory and field measurements on 500,000-circular-mil cables. (The data below are based on the writer's paper, "Impedance of 3-Phase Secondary Mains in Non-metallic and Iron Conduits.")[6]

Table VI

Laboratory Tests	$\frac{R_{ac}}{R_{dc}}$	
Triangular in 4-inch fibre conduit	1.03	Ratio: 1.10
Triangular in 4-inch iron conduit	1.075	
Flat in 4-inch fibre conduit	1.03	
Flat in 4-inch iron conduit	1.105	
Field Tests		
Random disposition in 4-inch fibre conduit	1.055	
Random disposition in 4-inch iron conduit	1.185	

In view of the smaller values in Table V of the discussion and the lower correction factor for randomness brought out by Table VI, a downward revision of the figures in Table XII of the paper is indicated at least for the 250,000-, 500,000-, and 750,000-circular-mil sizes.

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2. EDDY CURRENT LOSSES IN MULTI-CORE PAPER-INSULATED LEAD-COVERED CABLES, ARMOURED AND UNARMOURED, CARRYING BALANCE 3-PHASE CURRENT, **Dr. A. H. M. Arnold.** *Journal*, Institution of Electrical Engineers (London, England), 1941, Part II, page 52.

3. PROXIMITY EFFECT IN SOLID AND HOLLOW ROUND CONDUCTORS, **Dr. A. H. M. Arnold.** *Journal*, Institution of Electrical Engineers (London, England), 1941, Part II, page 349.

4. See reference 1 of the paper.

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6. IMPEDANCE OF 3-PHASE SECONDARY MAINS IN NONMETALLIC AND IRON CONDUITS, **L. Brieger.** *Bulletin*, Edison Electric Institute (New York, N. Y.), February 1938, pages 61–6.

F. H. Buller (General Electric Company, Schenectady, N. Y.): Though I am not a member of the Insulated Power Cable Engineers Association committee which was responsible for the work reported by Wiseman in his paper, I have had the privilege of being associated with this committee ever since the work started, and it is my opinion that the Electrical Testing Laboratories have done a fine piece of work in obtaining the data presented in Wiseman's report. While it is evident that the work as it stands is not complete (particularly in the field of segmental cables) nevertheless the data reported by the author represent an important contribution to our knowledge of a-c losses in cables in magnetic pipes; and it is to be hoped that additional data will become available in the future to supplement those in this paper, and to fill out the gaps therein, the existence of which Wiseman fully and properly admits.

A few additional comments are in order. The author states that the skin effect observed on the 2,500,000-circular-mil segmental cable tested in air agrees well with the 44 per cent rule. The General Electric Company has always used a 50 per cent rule, based on a variety of test data from several sources. Here is how the data compare:

Source of Data	Skin Effect	Remarks
Electrical Testing Laboratories	1.12	Test
44 per cent rule	1.11	Calculated
50 per cent rule	1.14	Calculated

This test indicates clearly that, while the 44 per cent rule gives a closer approximation to the test than the 50 per cent rule in this case, it is nevertheless not on the safe side; and, on the basis of this one test it does not appear advisable to us to depart from the 50 per cent rule.

Skin effect in air, as reported from the Electrical Testing Laboratories tests, appears to agree very well with the handbook data; however, the agreement between the test data on total losses on cables in the pipe and Atkinson's empirical formula is not at all impressive (see Table VII of the paper). This empirical formula as it stands should be used with the greatest caution in attempting to extrapolate the test results.

It is in fact rather surprising that almost one-third of the author's paper, is devoted to an empirical formula which gives results as erratic as Atkinson's. To do that gentleman justice, however, the pages devoted to the formula do not all represent wasted space; the Arnold proximity effect formula for cables in air, while well-known in England, is not nearly as widely publicized as it should be on this side of the Atlantic. In presenting this formula, Atkinson has done the industry a service.

It would be very desirable to obtain additional data on random-arranged cables. Some engineers contend that triangular arrangement is automatically obtained when three cables are pulled into a pipe simultaneously (or hexagonal arrangement when there are six). They base their opinion on the observed fact that the cables are twisted together as they leave the pipe. If, however, this opinion were correct, the cables would be completely transposed every few feet in the pipe, and their reactances to neutral would be identical. Field tests show clearly that these reactances vary widely, in actual practice. Hence the twisting must be confined to a relatively short length at the pulling end of the cables. We are told also that this conclusion is borne out by certain mechanical investigations which have been made in the field. It would thus appear that random arrangement rather than triangular arrangement actually prevails in practical cable lines.

It should be noted that the tests reported by Wiseman did not involve shielded cables, or cables with pulling armor (skid wires). Such skid wires and shields contribute materially to the losses in the system where conductor sizes are large; tests are now underway to determine their magnitude exactly, and to check it against theory. Results to date indicate clearly that such losses are large enough to increase over-all a-c losses considerably above the values obtained on the Electrical Testing Laboratories tests, where large conductors are involved. It is expected that the results of these tests will fill yet another gap in the present report.

E. S. Halfmann (Philadelphia Electric Company, Philadelphia, Pa.): Wiseman's paper presents much valuable data in a field where virtually no published information exists. It is unfortunate, however, that the test program was not extended to cover certain additional factors which would be extremely useful to the users of large size cables. Two examples will be given.

The first is the effect of compact segmental stranding on the a-c/d-c ratio for different sizes of conductors. Since tests on only one cable of segmental construction have been reported, it is not possible from data supplied in this paper to determine the variation of the a-c/d-c ratio with conductor size. In cables of segmental construction, the a-c/d-c ratio is probably also affected by the shape of the individual segments and by the number of segments insulated. Compact-segmental stranding is now used exclusively by the Philadelphia Electric Company for paper- and varnished-cambric-insulated cables in sizes larger than 1,000,000 circular mils, and hence the importance of this factor can readily be seen.

The second factor is the introduction of additional heat loss due to the presence of metallic shielding tapes, reinforcing tapes and skid wires which are normally used on pipe-type cables. No outer conducting tapes were used on any of the test cables. Experience indicates, however, that considerable additional loss can be introduced by these outer tapes, the exact magnitude of this loss depending on the particular taping construction used by the individual manufacturer. Results of tests on samples of 2,000,000-circular-mil compact segmental pipe-type cables, made and tested for us by two manufacturers, indicate a-c/d-c ratios of 1.86 and 1.98 at 60 degrees centigrade. These values are considerably higher than Wiseman's figure of 1.79 on an even larger size of cable (2,500,000-circular-mil) without metallic taping.

These examples serve as a warning that extreme care must be exercised if the expected advantages of very large conductor sizes are not to be lost because of unexpected additional losses in the conductors and in the metallic tapes.

M. H. McGrath (General Cable Corporation, New York, N. Y.): The paper by Wiseman is without doubt a contribution that will be of interest and value to cable engineers everywhere in connection with problems relating to the current rating and voltage drop of large-size conductors installed in steel conduit. When the research program covered by this paper was initiated prior to the war, primary emphasis was being placed on the need for resistance and reactance data of nonleaded low-voltage cables with rubber, rubber-like or varnished-cambric insulation installed in steel conduit in power stations or industrial plants.

Since the war, there has been a great increase in the use of impregnated-paper insulation in pipe-type systems for high-voltage transmission feeders. As a result, the present-day need for detailed knowledge of the many factors influencing the a-c losses of large size conductors in steel conduit, far transcends in economic importance anything one might have had reason to believe would be the case at the time the program was initiated. It would appear, therefore, that there is need for new material, new advice on this all important phase of high-voltage cable.

The data given demonstrates that the placement of a steel pipe about a 3-phase cable group, not only produces an extra a-c loss in the steel of the pipe itself, but in addition, may very materially increase the loss actually generated in the conductors. In the past, this point has received little, if any, attention in the technical literature. Investigators have invariably merely reported the over-all difference in a-c/d-c ratio with and without the pipe in place, thus giving no indication as to relative proportions of the extra loss developed separately in pipe and conductor.

The effective calorimetric method described in the paper removes our knowledge of the distribution in question from the unsatisfactory "conjecture" category existing in the past to one in which the distribution can now be expressed in numbers. As invariably happens in such cases, it can be confidently expected that the ability to obtain numerical knowledge of the distribution will lead to the development of refinements in existing conductor designs which will reduce the loss occurring therein.

For cable applications in station areas or in industrial plants, the data presented also provides a relatively accurate means of determining the gain to be obtained by use of nonmetallic fibre or transite conduit in place of steel for 3-phase groups, and also permits a more accurate evaluation than has been practical in the past, of the improvement with cables installed in separate nonmetallic ducts in place of a single conduit. These comparisons were made possible by the inclusion in the paper of the test data for both "in air," as well as the "in pipe," cases.

The relatively large difference in absolute magnitude of both the "in air" and "in pipe" values reported for the 2,500,000-circular-mil conventional concentric strand as compared to the corresponding values for the same size compact-segmental strand, indicates that when cables are installed as a 3-phase group in contact with one another, a check of the relative improvement possible with low skin-proximity effect designs such as annular or segmental will be justified, particularly whenever conductor sizes of about 1,000,000-circular-mils or larger are involved.

Furthermore, the approximate, but nonetheless entirely adequate relationship between the "in pipe" a-c/d-c ratio and temperature established in Wiseman's paper will in many cases permit a worthwhile improvement in the accuracy of current rating calculations, particularly where the higher a-c/d-c ratios are involved. In the past, lack of appreciation of this relationship has often necessitated the employment of laboratory room temperature measurements without correction to allow for the decrease at maximum permissible copper temperature. As shown in Wiseman's paper, there may be an appreciable difference in the two values.

The General Cable Corporation has for some time been conducting an extensive series of ac/dc measurements relating primarily to high-voltage pipe-type cable, and it is anticipated that the results of this investigation will be available for publication in the near future.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): The members of the Insulated Power Cable Engineers Association under the chairmanship of Wiseman are to be commended for their efforts to add to our knowledge of the a-c resistance of power cables. In view of the expansion in recent years of the high-pressure pipe-cable systems, our interest in the a-c resistance of cables in steel pipes also has necessarily increased.

A. H. M. Arnold's papers on the a-c resistance of power cables present useful tools in calculating the a-c resistance of such cables in air. The subject of a-c resistances in pipe has received considerably less attention and the present Insulated Power Cable Engineers Association data are, therefore, particularly welcome.

Unfortunately for some of us interested in high-pressure pipe-cable systems, Wiseman's paper necessarily will be an Interim Report. The known high a-c resistance of standard concentric stranding in cable sizes above 750,000- or 1,000,000-circular-mils precludes the economic use of such stranding, especially as the desired capacity of proposed extra high-voltage circuits may require the largest practicable conductor sizes. This situation presents itself also in lower voltage circuits in which lesser power requirements, nevertheless, at the lower voltages, may still mean copper sizes in the 1,500,000- to 3,500,000-circular-mil range. Here again, because of known relatively high a-c resistance, concentric stranding has generally been abandoned in favor of, first, rope core conductors and later, the segmental conductor design.

The Consolidated Edison System has only a limited amount of low-voltage concentric cable above 750,000-circular-mil size installed three per pipe. More generally, such cables are installed one per nonmetallic duct with sheath loss suppression methods in effect, leaving skin effect and to some extent proximity effect as resistance factors of interest. Generator cable leads of older vintage are of the rope-core type. The more recent installations, where not metal-clad, are of segmental conductor design. Either rope-core or segmental-type are, however, installed one per duct in sizes above 1,000,000-circular-mils.

Our interest in the Consolidated Edison System in large size power cables installed in steel pipe is confined primarily to extra high-voltage high-pressure pipe cable systems. Of this type, three circuits are now in service at 138 kv, comprised as follows: approximately 5 miles of 350,000 circular-mils (concentric); and 16 miles of 1,500,000 circular-mils (segmental). Approximately 22 miles of 1,500,000-circular-mil segmental conductor circuits are authorized and scheduled for service in 1950.

It is appreciated that the tests initiated by the Insulated Power Cable Engineers Association were not intended primarily for high-pressure pipe circuits, as the major emphasis in these tests is on concentric-stranded conductors. It is fortunate that the appropriation fund allowed tests on at least the 2,500,000-circular-mil size.

Table VII. Comparison of A-C and D-C Resistance-Field Tests—Random Spacing

	Test at 20 Degrees Centigrade Total Resistance, Ohms				Computed at 70 Degrees Centigrade (Myerhoff Method) Total Resistance, Ohms			
	A-C	D-C	Difference	Ratio	D-C	Difference	A-C	Ratio
Circuit A	0.527	0.284	0.243	1.86	0.340	0.243	0.583	1.72
Circuit B*	0.488	0.288	0.200	1.69	0.345	0.196	0.541	1.57
Avg								1.64

* The data of test "B" was taken originally on a circuit of pipe-cable plus oil-filled cable in series. Values for the pipe-cable portion of the circuit alone were derived using other test data for the oil-filled cable portion of the circuit.

Additional test data on, say, a 1,000,000- or 1,500,000-circular-mil segmental cable made under the same test conditions would have been particularly welcome. As Wiseman reports, tests have or are being made on several other conductor sizes and this data will be of great interest to the industry. These tests, at least in part, will be on impregnated cables and it is essential that the effect of dry versus oiled stranding be resolved.

The Consolidated Edison Company has recently completed field tests on two 8-mile pipe circuits of 1,500,000-circular-mil segmental cables. The results of these tests have not been completely analysed but the data shown in Table VII of the discussion are presented to illustrate the level of a-c/d-c resistance ratio obtained on field installations with random spacing.

The test results given in Table VII of the discussion are based on field measurements made without the precise degree of control exercised in the Electrical Testing Laboratories test. The results are, however, believed to be correct within a few per cent. The cable of both circuits is installed in $8^3/_8$-inch outside diameter pipe. The insulation is 560 mils and although the cable of each circuit was furnished by a different manufacturer, the designs are generally similar, differing only slightly in some repects as to cable makeup. Additional test data will be made available to the industry as authorized lines are installed. The average a-c/d-c resistance ratio of 1.64 at 70 degrees centigrade obtained for the two tests listed in Table VII appears rather high in comparison with some laboratory test values which have been presented to us for triangular spacing.

W. A. Sinclair (The Detroit Edison Company, Detroit, Mich.): Del Mar, Salter, Wiseman, and the Insulated Power Cable Engineers Association have made a very valuable contribution to the data required for determining ratings of pipe-type cables. The a-c resistance of large size conductors in steel pipe has been a very controversial subject and requires settlement if this type of cable is to be operated economically.

Results of a series of tests conducted on the 35,130-foot Delray-Warren line of the Detroit Edison System provide an excellent opportunity to check the tables set up in this paper for flat or random spacing. This 120-kv high-pressure gas-filled cable line consists of three *SMD*-type single-conductor cables installed in a $6^1/_2$-inch inside diameter by $^1/_4$-inch wall, mild steel pipe. The 600,000-circular-mil concentric-round conductors are insulated with 600-mils preimpregnated paper over which is a bronze tape intercalated with saturated cotton tape, cotton webbing tape lapped, and two half-round copper skid wires 200 by 100 mils applied open spiral, double start. The diameter over the skid wires is approximately 2.315 inches which means that for this particular line 38 per cent of the pipe is occupied.

Before circulating any current in the conductors, the d-c resistance of the total cable was measured by means of a portable Kelvin double bridge. This value, along with measured temperatures, was used as a reference value for all conductor temperature computations.

Test current for the positive sequence impedance measurements was supplied by six 500-kva transformers connected to give an open-circuit phase-to-ground voltage of about 1,290 volts. Table VIII of the discussion shows the impedance values obtained with 530 amperes along with the respective calculated values of resistance and reactance.

Comparing the a-c/d-c resistance ratio values in Table VIII of the discussion to those given in the paper, we find the 1.26 value to be 7 per cent higher than the values given in Table VIII of the paper for triangular arrangement. This would indicate the assumed 15 per cent increase in total losses, due to random arrangement over triangular arrangement of the conductors in a pipe, is on the conservative side.

K. W. Miller (Armour Research Foundation, Chicago, Ill.): For several years I have not been involved in underground cable problems. However, on the basis of past activities, I retain considerable personal interest in them.

On the basis of work which I did many years ago on the resistances and reactances of single conductor 3-phase circuits with various configurations of the cables, I recall that whenever there is departure from equilateral triangular symmetry of the cables themselves or of their inductive coupling with surroundings, then there is introduced a nonequality of impedances of the individual cables. Perhaps it is best to consider these inequalities, as introduced by unequal transformer action between the conductors or phases inasmuch as, if I recall correctly, under some extreme conditions, apparent losses may actually become negative in one phase or conductor. Therefore, it is not surprising that the authors found considerable inequality in impedances of conductors in their tests, where the cables themselves were not strictly equilaterally spaced or the center of the group as a whole was eccentrically located with respect to the center of the pipe or conduit.

Several years ago, it was practice on the Commonwealth Edison System in Chicago to align ducts with thin sheet iron conduit bands to hold them in place while pouring the concrete of the conduit structure around them. Although the total cross-section of magnetic iron surrounding a single conductor in a length of cable from manhole to manhole totaled only a square inch or so, and even this in time rusted away, nevertheless, oscillograph measurements indicated that a large amount of harmonics was introduced into the induced sheath voltages due to the nonlinear magnetic character of these iron bands. Perhaps oscillograph measurements were taken in some of the tests reported by the authors, and if so, it would have been of great interest if the results had been described in one or more of the papers. Personally, I would expect a considerable harmonic content in the resistance and reactance voltage drops even though all three phases are contained inside a single pipe or conduit, because the heavy iron wall of the conduit and the fact that the individual cables are necessarily separated by distances comparable to the radius of the conduit should to a considerable degree make up for the fact that phase summation of currents in a conduit is zero.

I am further unconvinced that resistance and reactance values are actually as independent of current magnitude as is reported. The current values listed in a recent paper[1] by E. H. Salter in Table IV vary by a ratio of only 3 or 4 to 1 at most and, it is important to note, tabulated values start with already sizeable conductor currents. It would seem probable that resistance and reactance values would both increase considerably for much smaller conductor current values, although, of course, the importance of such increase in R or X might not be particularly important, inasmuch as it is the I^2R and IX products which may be of chief concern. On the other hand, it is theoretically evident that as the currents I become indefinitely large, the resistance and reactance values must approach the same values as would exist with the iron conduit or pipe entirely ab-

Table VIII

Test Number	Conductor Temperature, Centigrade	Impedance, Ohms	Phase Angle	A-c Resistance, Ohms per 1,000 Feet	Reactance, Ohms per 1,000 Feet	D-c Resistance, Ohms per 1,000 Feet	A-c/D-c Resistance Ratio
1	11	2.149	68.71	0.0222	0.0570	0.0168	1.32
2	36	2.189	67.41	0.0239	0.0575	0.0185	1.29
3	54.5	2.197	66.28	0.0250	0.0572	0.0197	1.27
(Estimated)	70	2.236	65.58	0.0262	0.0579	0.0208	1.26

sent. I do not question the essential accuracy of the approximate constancy of the data reported, but, in the above remarks, point out that, in general, on theoretical grounds, this may not always be true. Fortuitous convenience of the reported results may be of questionable application where conditions of permeability, conduit, pipe, or cable size, or other geometric ratios are essentially different.

For practical computations on engineering accuracy, however, in a field where precise calculations are virtually impossible, the measured and reported empirical values covering as wide a field as they do, doubtless will be the most helpful, and the authors are to be commended for making these results available.

REFERENCE

1. PROBLEMS IN THE MEASUREMENT OF A-C RESISTANCE AND REACTANCE OF LARGE CONDUCTORS, E. H. Salter. AIEE TRANSACTIONS, volume 67, part II, 1948, pages 1390–97.

E. E. Hutchings (British Electrical and Allied Industries Research Association, London, England): The results given in test number 1 in Table I of Wiseman's paper agree well with the theoretical calculation of skin effect, since in this test the spacing is such that proximity effect would be negligible. The additional increment of resistance due to the latter effect in test number 2 is, however, very much greater than would be expected on theoretical grounds at the spacing indicated. Proximity effect for three cables in triangular formation in air, closely spaced, is given to a close approximation by the expression—

$$y_1 = 1.5(d/s)^2 y$$

where

d = diameter of the conductor
s = axial spacing between conductors
y = increase due to skin effect.[1]

The losses in the conductors in air, in the absence of adjacent metalwork, can be measured and compared with theory on well-established principles.[2] Very close agreement has been obtained in tests, similar to those described in the papers, which were carried out by the British Electrical and Allied Industries Research Association.[3] The increase in resistance due to proximity to steel in the form of plates parallel to the conductors is less than 0.002 microhm per centimeter at 12 inches from the steel for closely spaced conductors (as in a pipe) and is independent of conductor size.[4] This is less than 2 per cent for the largest conductors considered in the AIEE papers. The excess losses when the conductors are run in a steel pipe can be compared with theoretical values calculated as for steel armoured cables by known methods[5] using the appropriate properties of the steel. Only an approximate value need be assigned to the permeability, since variation of this over a wide range has but little effect. Unfortunately, only the nominal diameter of the pipes and their resistivities are given in the present papers. Full dimensional data are required in order to check the experimental results against the theory.

A similar investigation to that described in the papers was carried out by the British Electrical and Allied Industries Research Association in 1939[6] and led to the following conclusions:

1. The current rating of two or three single conductor cables is not appreciably affected by running them in steel pipes.

2. The total losses in the steel are in reasonable agreement with those deduced from the theory of armour losses and would be but little affected by the magnetic properties of the steel for ordinary pipes. These losses are substantially independent of the symmetry of the cable system with respect to the pipe, so long as the cables are closely spaced.

3. The volt drop may be materially increased by running cables in steel pipes. This is largely due to the increase in reactance. The increment of reactance will usually be of the order 0.3 microhm per centimeter, in accordance with approximate theory. The impedance was found to be increased by 30 per cent in the worst phase with 0.5 square inch cables due to their installation in a $3^1/_2$-inch diameter steel pipe, approximately 7/32 inches thick.

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2. **Wedmore, Whitehead, Morgan.** *Journal*, Institution of Electrical Engineers (London, England), volume 67, March 1929, page 367.

3. **Whitehead, Morgan, Hutchings.** Report Ref. F/T61, British Electrical and Allied Industries Research Association (London, England), 1933.

4. **Booth, Hutchings, Whitehead.** *Journal*, Institution of Electrical Engineers (London, England), volume 83, 1938.

5. **Whitehead.** *Journal*, Institution of Electrical Engineers (London, England), volume 88, part II, February, 1941.

6. **Hutchings, Whitehead.** Report Ref. F/T145. British Electrical and Allied Industries Research Association (London, England), 1940.

R. J. Wiseman: We appreciate very much the interest shown in this and the other two[1,2] papers reporting on the study made for Insulated Power Cable Engineers Association by the Electrical Testing Laboratories on large size conductors in conduits or steel pipes.

We would like to state a few facts which we hope will clarify for some of the discussers, views which they have presented. We are fully cognizant that this study is very limited in scope and should have been carried out on a much broader scale. Unfortunately, our funds were quite limited and, therefore, we tried to cover the primary objective as far as it was possible to do. This meant we were concerned with rubber- and varnished-cambric cables more than paper-insulated cables. This accounts for using a dry paper cable. A thin wall of insulation was used as it more closely followed low-voltage cables. However, we did make tests with several spacings of the 750,000-circular-mil conductors to indicate the trend for heavier walls of insulation. We should have had cables with shielding tapes over the insulation, but we did not have the funds to do so. Also it was recognized that such designs were special as far as Insulated Power Cable Engineers Association was concerned. We do regret mostly that only one size of segmental conductor was tested. If we had been able to make tests for several sizes, as for concentric stranding, we might have been able to set up a complete table of a-c/d-c ratios for all conductor sizes as we have done for concentric stranding. Personally, I am using the empirical formula for correcting for proximity effect found applicable for the 2,500,000-circular-mil segmental conductor and tests made on smaller sizes indicate that the a-c/d-c ratio obtained is less than the calculated.

Several have commented on various factors influencing the a-c/d-c ratio, such as temperature, current strength, pipe size, spacing, and so forth. This is all true, but we must keep in mind that it is necessary to set up a tool which is easily applied without too many correction factors to meet the particular conditions for each specific case. If we had to do so, we would never get any work done, so we agreed to set up a single curve for the a-c/d-c ratio which would be close enough to the correct value to be within the accuracy of other factors such as thermal resistivities. This meant that we use a ratio which would be that for a temperature close to the usual cable temperatures, that is, for full current load. We selected the ratio at 70 degrees centigrade as many cables today are operating at this temperature and some above it. For those above 70 degrees centigrade the actual ratio is slightly lower than for 70 degrees centigrade. Therefore, we are conservative. For cable setups operating at less than 70 degrees centigrade the actual ratio is higher than for 70 degrees centigrade. Therefore, the a-c resistance computed will be less than the actual resistance and the current allowable higher than it should be. The error will not be great, but if anyone is actually concerned they can use the empirical formula method for calculating the a-c resistance more accurately.

We realize this research is only partially completed, not over half done, and I hope that we will somehow find funds to carry on this work. It is important to the industry as a whole.

Referring to specific discussions. Answering Miller, we did not make oscillograph measurements during tests. We agree that the wave shape is probably upset. We started our tests at 750,000-circular-mil conductors as we were interested in large size conductors. However, low-current values were used to obtain the influence of temperature on the resistance and reactance. Therefore, we had low-current density. Likewise the highest temperature was slightly over operating temperature, so we had current densities as high as would be expected in service. Going further than this is not necessary for our purposes.

We recognize Halfmann's warning regarding the use of segmental conductors and it is regrettable we did not have the opportunity to make more tests on this conductor design. The variation that he reports on a 2,000,000-circular-mil conductor needs consideration from all the construction variations that may exist between the two designs.

Buller questions the use of the 44 per cent rule for determining the skin effect factor for a segmental cable. This matter was discussed by the Insulated Power Cable

Engineers Association years ago and data was presented by several companies as to what factor should be used. It was agreed to in the Association that the 44 per cent rule had more basis, in fact, than the 50 per cent rule and the former was adopted. The Electrical Testing Laboratories tests made on the 2,500,000-circular-mil segmental cable in air seems to check this decision.

Buller objects to taking too many pages to present the empirical formula proposed by Atkinson. I feel we are indebted to Atkinson for proposing a means for calculating the a-c/d-c ratio for any conductor size. To me, we took just as much liberty in plotting a curve and then taking from its ratios for any conductor size when we actually had only six points and four of these were for the same conductor size; therefore we really plotted a curve with three points. What I like about Atkinson's empirical formula is that it gives us a picture of what may be the proportional of increased resistance due to proximity and the presence of the pipe. We do not claim the empirical formula to be the last word, but it is surely better than anything to date that we have. In view of its newness it was necessary to discuss it in detail; therefore, it took a large number of pages to present it. I take full responsibility for this. I do not consider the values obtained by the empirical formula as erratic. Considering the cable construction that we are dealing with, I think it is remarkable that a simple formula was found which could be used practically and does check actual tests. We are not looking for an accuracy of 0.1 per cent. We are glad to know if we are as near as 1 per cent.

We agree with Buller that further information is needed on random arrangement of cables in a pipe. There are strong views both ways as to how cables are positioned when drawn into pipes. Therefore, it is best that we leave it to further installations before we draw any final conclusions.

We like very much the manner in which McGrath has described the over-all picture as to the necessity of having the true a-c resistance and reactance of cables in steel pipes. We wish to state that McGrath materially helped the committee in the study of the test data and contributed very constructive comments in interpreting the results.

The tests reported by Hatcher on the two 8-mile circuits of 1,500,000-circular-mil segmental conductors are very helpful and will be included in the compiling of actual field tests. We agree with Hatcher that the results seem rather high in comparison with laboratory test values which some of us are finding for triangular spacing. It shows the necessity of further investigation.

We are indebted to Sinclair for contributing tests made on the 120-kv high-pressure gas cable in the Detroit Edison Company. Calculation of the a-c/d-c ratio using the empirical formula gives the results shown in Table IX of the discussion. The actual ratio is roughly 5 per cent higher than the computed. This may be due to random arrangement as Sinclair believes or possibly accuracy in measuring the a-c watts.

Unfortunately, time has not been available to devote to further study of this subject along the lines suggested by Roehmann with reference to the use of Arnold's formulas for the effect of armoring on the losses in cables. I believe he has a good point. Knowing Roehmann's interest in this subject, perhaps he or A. C. Connell of Anaconda Wire and Cable Company will take the time to make such calculations for us and report them to the committee for incorporation in the Insulated Power Cable Engineers Association report.

We appreciate the interest that Brieger has taken in this study and his discussion is going to be helpful to all of us who are giving this subject consideration. When we were considering the empirical method for calculating the a-c/d-c ratio, we were acquainted with Arnold's work. Perhaps I should have taken the working formulas for the 3-phase system in air instead of that for the single-phase system, but I looked upon it as a means to an end, that is, we hoped to find a factor which, when multiplied by the proximity effect, would give the proximity when cables were installed in pipe. What it means is that we would have to use the same procedure but the factor would be different according to whether we start with a single-phase or a 3-phase formula, but the end result would be the same. It seems to me as if it is much simpler to use the single-phase formula.

It was intentional when the factor K in the Arnold formula for the interstrand resistance correction was taken as $K=1.0$. This is a variable depending upon the degree of contact between strands, oxidation of surface of strands and whether strands are dry or oiled. All of these will vary according to the manner in which a conductor is manufactured and every time the conductor is made. Brieger gives suggested values which vary with conductor size and temperature. We do not believe it is possible to arrive at a specific value of K; therefore I assumed it to have a value $K=1$. This means that there is this variable present in actual practice and we can not expect accurate check of computed and actual values. We are trying to get as simple a formula as will give a better indication of what a-c resistance we can expect to have when cables are installed in pipes than that which we have had heretofore and be within practical accuracy.

Reference to Curtis' work is most valuable and the agreement between P_1 and his values of a-c/d-c ratio shown in Table I of the discussion is quite remarkable. The curves in Figure 1 of the paper could be used instead of going through the calculation by the Arnold formula.

As in the case of having only one test for segmental conductors we only had two tests for flat arrangement. Many more are needed before we can draw more accurate conclusions. The data reported by Brieger in Tables III, V and VI of the discussion indicate that it is necessary to proceed cautiously on the influence of flat or random arrangement and that the 15 per cent factor we have assumed because of the 2,500,000-circular-mil tests is too high. Perhaps we should not go over 10 per cent.

I wish to repeat that the committee recognizes its work is only partially completed. Field tests being reported as pipe-type systems are installed indicate a modification of our conclusions may have to be made. This would be speeded up if it were possible to obtain funds to carry out laboratory studies on various combination of construction as a guide to their influence. This is a long time proposition. It is ideally suited to a university research laboratory for students studying for advance degrees. Can we find a source of funds and a university who will undertake the task?

We are very glad to receive Hutthing's discussion, knowing the interest he has had for many years in losses in power cables.

In no case does the law he gives for proximity in air apply to any of our tests with cables in air in triangular formation. If the factor 1.5 was changed to 2.0 better agreement would be obtained.

I think it worthwhile when time permits to make a comparison of both the Arnold formula for the effect of armoring on the losses in cables and the Booth, Hutchings and Whitehead method.[3] Perhaps Roehmann can make both calculations. We should keep in mind that in the case of armored cables we have the magnetic jacket close to the conductors, whereas for cables in a pipe only where the cables rest on the bottom of the pipe are the conductors and the magnetic jacket close together. This should influence the values found.

We are planning to send to Hutchings the full dimensional data on the pipes so that he can check theory and experimental results.

The conclusions arrived at by British Electrical and Allied Industries Research Association in 1939 are quite interesting. For small conductor sizes the first conclusion is correct, but for large sizes which we are primarily interested in for pipes, there is a decrease in allowable current as compared to cables in duct.

The second conclusion as to total losses can be checked when Hutchings makes a comparison between the theory of armor losses and that which was found on our tests. Reference to the effect of cable symmetry is interesting. We do keep the cables closely spaced because we try to use the smallest pipe size possible in order to hold costs down. It indicates that if random arrangement does take place, the increase should not be great. This also seems to be indicated in the Detroit Edison Company tests.

Table IX. AC/DC Ratio

Test Number	Temperature Degrees Centigrade	Detroit Edison Tests a	Computed b	a/b
1	11	1.32	1.27	1.04
2	36	1.29	1.23	1.05
3	54.5	1.27	1.21	1.05
Est.	70	1.26	1.19	1.06

REFERENCES

1. See reference 2 of the paper.

2. See reference 3 of the paper.

3. CURRENT RATING AND IMPEDANCE OF CABLES IN BUILDINGS AND SHIPS, H. C. Booth, E. E. Hutchings, and S. Whitehead. *Journal*, Institution of Electrical Engineers (London, England), volume 83, 1938.

Causes and Effects of Stray Electric Currents in Coal Mines

L. H. HARRISON
MEMBER AIEE

THE HAZARDS represented by stray electric currents in coal mines and earth strata have been a major problem since electric shot-firing methods were introduced some years ago. The accident records of the mining industry are replete with instances of premature explosions and shot-firing accidents of various kinds for which the only logical explanation is stray or uncontrolled electric currents in the earth.

The sources of these stray electric currents are, in all probability, numerous and varied. It is not the purpose of this paper to enumerate all possible sources; however, two principal sources of such currents in the earth are:

1. Lightning discharges to the surface of the earth.

2. A combination of power circuits and electric equipment used for the normal operation of the mine.

The first source is, of course, one about which little or nothing can be done. The second source is largely preventable, although complete elimination of such currents, even from these sources, is virtually impossible. Certain precautionary measures can be taken, but the effectiveness of such measures can be greatly increased by well-planned electrical installations and proper maintenance.

Some Blasting Accidents Caused by Stray Currents from Power Circuits

An air shaft was being sunk from the surface when a premature blast occurred. The shaft was down about 150 feet and blasting was done electrically and fired from a 250-volt power circuit. The men were hoisted and lowered in a metal bucket which was moved by means of a d-c motor-driven electric hoist. A round of holes had been drilled and charged. Connections were made to the firing line which was locked open on the surface and short-circuited. The firing-line switch on the surface was a double-pole type, and two wires were brought from it to the bottom of the shaft. The men prepared to go to the surface. They signaled the hoist operator and climbed into the bucket. The rope tightened, suspending the bucket; but it swung to one side of the shaft, came in contact with the firing wires, and exploded the charge.

The actual cause of this accident was never definitely determined. Investigators believed that a portion of the return current from the hoist motor traveled down the hoist rope and through the explosive charge into the earth. No fault was discovered in the hoist motor or its control equipment, however, and there is also a possibility that the flow of current was from the earth to the hoist rope through the explosive charge. Faulty track bonding or high-resistance grounded return circuits could be responsible for such an accident.

At an Alabama mine the face of a room had been drilled and charged. The shot-firing wires were short-circuited near the room neck. A water-pipe line was installed in the room, and bare spots in the firing line were in contact with it. A locomotive was switching cars in an adjoining room at the time that the miner connected his firing line to the lead wires, exploding the shot. In this instance it is thought that a portion of the electric current from the locomotive operating in the adjoining room flowed over the pipe line, causing a difference in potential between two bare spots in the firing line; the current was powerful enough to detonate the charge.

Another shot-firing accident in the same mine severed a miner's hand at the wrist. The miner had prepared the primer and was carrying it to the face. A mine car was in the place about eight feet from the face and as he passed alongside of the car, the ends of the detonator lead wires of the charge in his hand were dragging along the bottom when the charge went off. In this instance it was thought probable that one end of the wire had contacted the rail and that enough current flowed from the mine strata to the rail to cause the explosion.

Another accident, which fortunately did not result in an injury, occurred as follows: Explosive charges in two holes in the bottom bench had failed to fire and were covered by loose coal dislodged by shots in the upper bench. The coal was being loaded out on the next shift by a mechanical loader. One of the holes was detonated by stray currents and the operator jumped away from the machine, leaving it running. The operator's helper was standing on the pile of loose coal nearby. When the second shot went off, he was toppled over into the machine conveyor. These shots were unquestionably exploded by stray currents, the source of which probably was the mechanical loader. This would be possible only if the frame of the loader were grounded to the negative or a fault existed in the electrical system of the loader itself. Such accidents emphasize the importance of proper maintenance and effective grounding in operating mining machinery.

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L. H. HARRISON is mining-electrical engineer, coal-mine inspection branch, Bureau of Mines, Birmingham, Ala.

Some Accidents from Stray Currents Due to Lightning Discharge

When a discharge of lightning occurs, the visible disturbance in the atmosphere is only part of the complete stroke mechanism. At the time of discharge, an electrical disturbance takes place within the orginating cloud formation. Moreover, a similar disturbance also is set up in the earth when discharges to the earth's surface occur. Electric currents of enormous intensity flow in the earth under discharge conditions. The earth offers resistance to the flow of current, and voltage gradients are built up in the earth's strata. Such voltage is enough to detonate explosive charges at a considerable distance from the point of entry into the earth.

Currents due to lightning, either direct strokes or from induced charges, are carried into the underground workings through mine tracks, pipe lines, or electrical feeder circuits and are dissipated in the earth. Differences of potential are thus created which have been known to cause the detonation of explosive charges several thousand feet from the mouth of a mine.

Short-circuiting of the detonator lead wires or shot-firing lines previous to the actual firing undoubtedly has prevented many premature detonations. This method, however, is not entirely effective in preventing such accidents. It is possible that heavy currents in the earth due to lightning discharges may cause a current to flow in the closed blasting circuit by the process of induction. Tests made at the Bureau of Mines' experimental mine, Bruceton, Pa., indicated that a voltage could be induced in an insulated wire buried in a shallow trench or lying on the surface. These experiments were made in connection with a ground conduction communication investigation. Voice-modulated currents at audio frequencies were fed into the mine strata by means of the mine track or grounding electrodes in the mine roof. They were picked up on a single insulated wire and on an insulated wire loop. The single wire was buried in a shallow trench about three inches below the surface. The loop was used with one side buried in the same trench and with the other side of the loop supported about two feet above the surface. The voice signals were picked up in this manner and amplified by means of a standard audio amplifier such as used in public address systems. The input current from the underground transmitter was in the order of a few milliamperes. The vertical distance through the earth strata from the point where the signals were fed into the earth was approximately 100 feet. Because of the minute currents involved in this experiment, it seems entirely possible that currents of sufficient intensity to detonate explosive charges could be induced in the closed loop of a shot-firing circuit under certain conditions of lightning discharge.

Abundant evidence has been accumulated to prove that lightning has caused the detonation of explosive charges in underground and open pit mines. Such instances have occurred under circumstances in which the secondary cause would appear to be electric currents flowing in the earth strata owing to lightning discharges from cloud formations to the earth's surface.

Two premature explosions in a limestone mine at Mullins, Ky., were attributed to lightning. In the first instance some six or eight holes had been loaded when the foreman in charge was notified that an electrical storm was in progress. He immediately stopped the loading of holes and came out of the mine with his crew. The wires from the several holes had not been connected but were left hanging loose. Shortly after the foreman and his crew reached the outside a keen clap of thunder was heard and lightning appeared to strike near the top of the hill under which the mine was located. Immediately thereafter, an explosion was heard in the mine. After the storm subsided an investigation was made, and it was found that the charges in six holes had been detonated.

In the second instance the circumstances were almost identical with the first instance, except that at the time the foreman was advised of an electrical storm occurring outside he had connected the wire and attached the lead line. As the foreman and his crew came out of the mine, he unreeled the lead line in such a manner that it was in contact with an exposed compressed-air pipe line. Shortly after the men had left the mine, there was considerable thunder accompanied by sheet lightning and all the loaded holes were discharged.

An article[1] written in 1933 gave a description of the premature firing of 16 of 36 holes in an underground limestone mine of the Washington Building Lime Company, Bakerstown, W. V. The article stated that the men had charged 36 holes and were some distance from the surface, the firing lines were not connected, and the cause of the premature blast was attributed to lightning.

The foregoing further emphasizes the fact that the utmost care must be exercised in the handling of electric blasting circuits, if premature blasting accidents are to be minimized. Firing lines should be well maintained and contact between the bare ends of lead wires and pipes, rails, or the mine strata itself should be avoided.

Stray Currents from Local Power Sources

The other main source of stray currents in coal mines, unlike lightning, presents a constant hazard. Stray currents from mine feeder systems and haulage tracks are ever present in the mine strata. They are carried to the working faces by track rails, pipe lines, wire ropes, grounding conductors, and so forth.

Perhaps the most prolific source of stray currents in coal mines is the haulage track. Electric trolley locomotives provide the most common type of haulage in mechanized coal mines, and the grounded track is the return power conductor for the locomotives and other equipment that receives power from the trolley circuit. The track ordinarily has ample carrying capacity to transmit the currents involved, and therefore it is usually considered wasteful and unnecessary to install feeder cables in the grounded portion of the circuits. The mine tracks inherently present a low resistance contact to earth, and thus the earth or mine strata becomes a coconductor with the mine track. Any difference of potential due to voltage drop in the track circuit also appears in the mine strata, and differences of potential can be measured between various points of contact in the mine floor, ribs, or roof. Such differences give rise to the flow of stray currents in the mine strata.

Some coal beds are laminated with bands of foreign material, such as clay or shale, that has a high moisture content and a fairly high degree of electrical conductivity. When locomotives or other electric apparatus operate in face regions, a portion of the current used by such equipment may flow through the bands of foreign material. This condition often is aggravated by poor bonding of tracks or the entire lack of bonding in face regions. Poor bonding or entire lack of bonding is one of the principal reasons for stray electric currents in the face regions of operating coal mines.

Water spray lines and frame-grounding conductors connecting the bonded mine track with the frames of mining machines and coal-loading equipment provide a low-resistance path for stray currents into the face of a room or wall. The

development plan of a mine often involves the installation of bonded tracks in parallel entries. When a room is driven from one entry, the face of the room may gradually approach an adjacent parallel entry. Two paths for the flow of current are thus set up, one in the first entry track and the other in the room track, pipe lines, grounding conductors, and so forth, to the face of the room, thence through a short section of the coal bed to the track in an adjacent parallel entry. The circuit of a d-c motor-driven machine may become faulted or grounded to the frame of the machine. If the fault occurs on the return side of the circuit the operation of the machine may not be affected. A poor return circuit for such a faulted machine may cause an appreciable amount of the machine current to flow in pipe lines or through the intervening strata or coal bed to tracks in an adjoining entry.

Another source of stray currents is leakage from an accidentally grounded conductor of a mine feeder system. Grounded power conductors frequently have caused mine fires owing to leakage of current heating and igniting the coal; this is conclusive evidence of the flow of current in the mine strata. In most instances the ground circuit presents a high impedance to the flow of current and may not be detected until after a long period of time. Simultaneous grounds may occur on opposite phases of a system at remote locations. Current will flow in the earth between such ground faults and may cause premature blasts under certain conditions.

On 3-phase Y-connected grounded neutral power systems, enough current may flow in the earth under fault conditions to cause premature detonation of explosive charges.

This may be true even where a grounding conductor is installed with the feeder system, unless provision is made for limiting the fault current by means of resistors or reactors in the grounding circuit.

Electrolytic action upon the metallic sheath or armor of power and telephone cables is another harmful result of stray electric currents. Frequently voltage readings from cable sheaths to ground will be found to vary directly with the load on d-c substations supplying mine loads. Such deleterious action, though not ordinarily hazardous to human life, results in damage to expensive cables and may result in costly outages.

Conclusion

The notoriously careless manner in which electrical installations are made in and around coal mines is an important factor in producing stray currents. The benefits to be derived by the reduction of such currents to a minimum are not always tangible. The Bureau of Mines for many years has recognized the dangers represented by haphazard and inadvertent methods of installing and maintaining electric equipment in coal mines. The Bureau has endeavored to bring about an improvement in the general quality of mine electrical installations by disseminating knowledge and by inspection and assistance. The Federal Mine Safety Code has been helpful in bringing about such improvement. Considerable progress has been made but much remains to be done. The day may not be too far distant when the electrical installations in a coal mine will compare favorably with those of any other industry having comparable problems. It can be done.

Reference

1. Be Careful of Electrical Storms. *Explosives Engineer*, (Wilmington, Del.) October 1933, page 294.

No Discussion

Report of the Board of Directors

THE BOARD OF DIRECTORS of the American Institute of Electrical Engineers presents herewith to the membership its 64th annual report, for the fiscal year ending April 30, 1948. It includes a general balance sheet and other statements showing the financial condition of the Institute at the close of the fiscal year and a brief summary of the principal activities of the Institute during the year, more detailed information having been published from month to month in *ELECTRICAL ENGINEERING*.

BOARD OF DIRECTORS' MEETINGS

Five meetings of the board of directors were held during the year, two in New York, N. Y., one in Montreal, Quebec, Canada, one in Chicago, Ill., and one in Pittsburgh, Pa.

Information regarding many of the more important activities of the Institute which have been under consideration by the board of directors and AIEE committees is published each month in the section of *ELECTRICAL ENGINEERING* devoted to Institute activities.

PLACES VISITED BY PRESIDENT HULL

California
Pacific general meeting, San Diego; Los Angeles Section; San Francisco Section

Colorado
Denver Section

Connecticut
North Eastern District meeting, New Haven

District of Columbia
Washington Section; George Washington University Branch, Catholic University of America Branch, Washington

Illinois
Midwest general meeting, Chicago; Illinois Valley Section, Peoria; Urbana Section

Iowa
Great Lakes District meeting, Des Moines

Kansas
Wichita Section

Louisiana
New Orleans Section; Shreveport Section

Maryland
Maryland Section, Baltimore; University of Maryland Branch, College Park

Mexico
Summer general meeting, Mexico, Federal District

Minnesota
Minnesota Section, St. Paul

Missouri
Kansas City Section; St. Louis Section

Montana
Montana Section, Butte

Nebraska
Nebraska Section, Omaha

New York
Meetings of board of directors, Engineers Joint Council, conferences, New York, N. Y.

Ohio
Middle Eastern District meeting, Dayton; Cincinnati Section; Cleveland Section; Columbus Section; Toledo Section

Oklahoma
South West District executive committee meeting, Oklahoma City

Oregon
Portland Section

Pennsylvania
Winter general meeting, Pittsburgh; Philadelphia Section

Texas
North Texas Section, Dallas

Utah
Utah Section, Salt Lake City

Washington
Seattle Section; Spokane Section

Wisconsin
Madison Section

ANNUAL MEETING

The annual business meeting of the Institute was held on June 11, 1947, in Montreal, Quebec, Canada. The annual report of the board of directors for the fiscal year which ended April 30, 1947, was presented in abstract by Secretary H. H. Henline, and the Institute's financial situation was reviewed by Treasurer W. I. Slichter. The report of the committee of tellers on the election of officers for the year which began on August 1, 1947, was presented, and President-Elect Blake D. Hull responded to his introduction with a brief address. National prizes for papers were presented.

The Lamme Medal for 1946 was presented to John B. MacNeill, manager of the switchgear and control division, Westinghouse Electric Corporation. The president's address by J. Elmer Housley concluded the meeting.

GENERAL MEETINGS

A brief report on each of the four general meetings held during the year follows:

Summer General Meeting. The 63d summer general meeting was held in Montreal, Quebec, Canada, June 9–13, 1947, with a registration of 1,451. The program included 13 regular technical sessions, 14 conference sessions, 10 administrative conferences, and about 25 committee meetings, in addition to the afore-mentioned annual business meeting. There were entertainment events, inspection trips, golf tournaments, a president's reception, a banquet, and a dance.

Pacific General Meeting. The Pacific general meeting was held in San Diego, Calif., August 26–29, 1947, with a registration of 589, a new attendance record for the meeting. There were 15 sessions, including two devoted to the presentation of student papers, a luncheon with address by President Hull, Student Branch conference and a conference of District and Section officers, inspection trips, a golf tournament, and numerous social and entertainment activities including a "Night in Old Mexico," with dinner at Serena's in Tijuana.

Midwest General Meeting. The first Midwest general meeting was held in Chicago, Ill., November 3–7, 1947, with a registration of 1,449. This meeting is to be held annually in the "Middle West." The Chicago meeting included three general sessions, seven technical sessions, and nine technical conference sessions, including one held jointly with the National Electronics Conference. About 40 meetings of general and technical committees were held. The program included a dinner-smoker with entertainment, many inspection trips, and numerous events for the women.

Winter General Meeting. The winter general meeting was held in Pittsburgh, Pa., January 26–30, 1948. The registration was 3,081. A total of 51 technical sessions included 18 for formally approved papers, 18 for informal conference papers, and 15 at which both types of papers were presented. Meetings were held by about 75 technical and general committees. The Edison Medal for 1947 was presented to Doctor Joseph Slepian. Other features were a smoker, a dinner-dance, many inspection trips, and a generous program of social and entertainment events for the women.

DISTRICT MEETINGS

Middle Eastern District Meeting. The first Middle Eastern District meeting since 1940 was held in Dayton, Ohio, September 23–25, 1947. The registration was 502. There were eight technical sessions and one general session. Papers presented included 28 formally approved and 12 conference papers. The program also included a banquet, inspection trips, and special features for the women. The District executive committee held an all-day meeting on September 22.

Great Lakes District Meeting. The eighth Great Lakes District meeting was held in Des Moines, Iowa, April 1–3, 1948, with a registration of 357. Seven technical sessions provided for the presentation of 13 formal papers, 8 conference papers, and 10 undergraduate and 7 graduate student papers. President Hull gave the principal address in the opening session on the subject "Ceiling Unlimited." There were several inspection trips, a banquet, a general luncheon, and numerous events for the women.

North Eastern District Meeting. This meeting was held in New Haven, Conn., April 28–30, 1948, with a registration of 669. The technical program included 17 formally approved papers, 27 District papers, and 11 student papers. There was a general session with four addresses. Other events were a District executive committee luncheon, a dinner, inspection trip, and various events for the women.

TECHNICAL CONFERENCES

A conference on electron tubes held in Philadelphia, Pa., March 29–30, 1948, and a conference on electrical problems in the rubber and plastics industries held in Akron, Ohio, April 20, 1948, introduced a new activity devoted to more extensive consideration of specialized technical subjects. The first was sponsored by the joint subcommittee on electron instruments of the committees on electronics and instruments and measurements, with the co-operation of the Philadelphia Section. The second was sponsored by the subcommittee on rubber and plastics of the committee on general industry applications and the Akron Section.

General Committees

FINANCE COMMITTEE

As seemed inevitable from recent trends, the fiscal year ending on April 30, 1948, showed a substantial operating deficit.

While we had a $75,500 (almost 14 per cent) increase in income, our expenditures increased $144,000 (or 27 per cent), resulting in a deficit of approximately $44,500. This deficit was met by withdrawal from our reserve capital funds.

For the budget year October 1, 1947, through September 30, 1948, the board of directors adopted a budget which, in spite of efforts to hold appropriations to a minimum consistent with the vigorous and progressive activities of the Institute, showed a deficit of some $23,000. This was after taking full account of the anticipated growth in membership and the substantial increase in advertising revenues expected to result from higher advertising rates and more active advertising sales effort. Subsequent experience has indicated that costs will be even higher than budgeted without corresponding increases in revenue, so that the budget year deficit is expected to somewhat exceed the original budget estimate but should not be in excess of the $44,500 operating loss for the fiscal year just ended.

Because these trends raise the obvious question as to whether continually increasing costs per member can be met without increasing payments per member, President Hull appointed a special committee to study the whole matter and recommend a course of action. Fortunately, we have a capital reserve fund that is adequate to tide us over any deficits that can be anticipated for the immediate future unless there should be a sharp recession with resultant drop in revenues from membership dues and advertising.

COMMITTEE ON PLANNING AND CO-ORDINATION

Further work has been done toward implementing the Institute's technical activities as reorganized last year and as outlined in the memorandum by Doctor Wickenden.

The four co-ordinating committees of the technical committees each have served, as the name implies, to harmonize and advance the activities of their respective technical committees. Committee scopes, membership lists, and agenda have been correlated and new work initiated. The co-ordinating committees warranted more formal status by defining certain general items of their scopes and this has been done by the committee.

A technical activities subcommittee was continued, the membership this administrative year consisting of the chairmen of the four co-ordinating committees. The purpose of this subcommittee was to correlate the activities of the co-ordinating committees, to keep the committee on planning and co-ordination informed as to progress being made in the co-ordination of the various technical activities of the Institute, and to serve as a medium of co-ordination on a national and on a Sections basis. This technical activities subcommittee has aided in eliminating duplication of technical effort by the Institute's technical committees with those of other organizations. It is clearly evident that the co-ordinating committee setup is working out admirably well. The committee recently has decided to augment the personnel of the technical activities subcommittee to include the vice-chairmen, as well as the chairmen, of the four co-ordinating committees, and also the chairman and vice-chairman each of the technical program and Standards committees.

There have been two groups in the Institute's organizational setup, one known as the administrative group and one as the professional group. The administrative group is not now serving any particular need, and the committee has recommended and the board of directors approved the discontinuance of this group at the end of the present administrative year.

The new scopes of the power group committees were endorsed and recommended to the board of directors for approval. Changes in names of certain committees in the industrial and power groups were considered and approved. The formulation of a new committee on computing devices also was approved together with suitable scope. The scope for the new committee on management was reaffirmed.

The committee has considered the invitations received and recommended to the board of directors specific locations and dates for future national and District meetings for the next three years. It is necessary that these be determined well in advance of the scheduled dates so that suitable arrangements can be made.

A new type of meeting recently has been conceived known as the "technical committee conference." This may be defined as a program of panel discussions, technical papers, or exhibits under the auspices of a national technical committee, or subcommittee. The purpose of these meetings is to expand technical committee activities on individual specialized subjects and to serve the interests of members and nonmembers on that subject. Two of these technical conferences have been held to date: one on the subject of electron tubes at Philadelphia during March and one on rubber and plastics in Akron during April. It has been reported that these conferences were extremely successful and the committee believes every encouragement should be given them.

A recommended policy for these technical committee conferences was established. This includes definition, scope, financial arrangement, and authorization for such conferences. The authorization is to be in the hands of the chairman of the co-ordinating committee affected and he will correlate with the technical program committee and others. The board of directors will need to be consulted only when financing is involved.

The subject of Institute prize awards is being restudied in detail. A subcommittee of the Sections committee has undertaken this work and a report with recommendations is expected later this year.

Work has been initiated covering the establishment of a publicity policy for the Institute. A statement of policy prepared by the committee on publicity has been approved by the committee on planning and co-ordination and by the board of directors.

There has been considerable discussion regarding the need for and the procedure to be followed in "Organization of the Engineering Profession." The committee has recommended and the board of directors has approved the following resolution:

RESOLVED:

1. That the resolution adopted by the board of directors at its meeting on November 5, 1947, be reaffirmed and that the board express its desire for prompt action.

2. That the board (of directors) ask its representatives of the EJC (Engineers Joint Council) to request very prompt action be taken on this matter.

3. That the board (of directors) authorize President Hull, and representatives designated by him, to accept invitations from the boards (of directors) of other national engineering societies to present the views of the AIEE.

Numerous proposed modifications of the Institute's constitution and bylaws have been reviewed and discussed. Certain changes have been approved for submission to the board of directors.

The Italy America Society, Inc., has offered the Institute certain funds, and suitable plans are to be developed for the administration of such funds if they become available.

PUBLICATION COMMITTEE

The new publication policy, which went into effect January 1, 1947, now has been in effect for 16 months, and in general is functioning successfully and satisfactorily according to plan. The full details of the new program were explained to the membership by the then chairman, C. F. Wagner, of the publication committee, in an article (*EE, Dec '46, pp 576–8*); and the essential substance of these same details was embraced in the 1947 report of the publication committee to the board of directors. A few necessary modifications and minor adjustments of details have been made according to the requirements of the circumstances as the new procedures have been developed and applied.

The plan provided for three basic correlated publications, *ELECTRICAL ENGINEERING*, *TRANSACTIONS*, and *PROCEEDINGS*. This program seems to be meeting very well the broad and diverse interests of the membership, according to questionnaires which were circulated at the 1947 summer general meeting and at the 1947 Pacific general meeting, and also as indicated by informal comments and discussions at other meetings and by correspondence received from individual members.

In summary, the highlights of the publication year are: the improvement in the physical appearance of *ELECTRICAL ENGINEERING*, the increase in the advertising content, and the improved scope and subject balance of the text material published; the adoption of a flat price for the advance pamphlet copies of technical program papers, regardless of the number of pages of the pamphlet, in place of the previous and more complicated sliding-scale price schedule; the successful initiation of the new interim publication service to the Institute members represented by the newly re-established *PROCEEDINGS*, and the related and surprising discovery that the actual membership demand for *PROCEEDINGS* is materially lower than initially was contemplated; the postponement of publication of the 1948 "Year Book" for the purpose of establishing a new and more effective publication cycle co-ordinated with the administrative year, which begins the first of August.

ELECTRICAL ENGINEERING. *ELECTRICAL ENGINEERING* continues as the general service publication which reaches all members, all Student members, and a substantial list of nonmember subscribers. With the release of previous space commitments for *TRANSACTIONS* pages in *ELECTRICAL ENGINEERING*, the scope and nature of subject matter published in the monthly has been, and still is being, progres-

Table I. Numbers of Pages of Published Material

	ELECTRICAL ENGINEERING			*TRANSACTIONS* Only		
Year	General Interest Articles	News	Transactions Section	Technical Papers	Discussions	Totals
1941	337	281	600	542	268	2,028
1944	246	214	738	493	231	1,922
1945	255	225	736	121	143	1,480
1946	285	315	832	254	120	1,806
1947	806	474	1,463*	153*	2,896*	

* Preprinted in 227 sections of AIEE *PROCEEDINGS*, except for a 17-page technical paper in *ELECTRICAL ENGINEERING*.

sively broadened to serve better the diversified interests of the membership and to supplement the complete formal technical papers published in *PROCEEDINGS* and *TRANSACTIONS*. Direct contact with these formal papers has been maintained for all readers of *ELECTRICAL ENGINEERING*, however, by virtue of the fact that either the full text or essential substance of a large number of technical papers has been published in *ELECTRICAL ENGINEERING* and at least a brief abstract of all the other *TRANSACTIONS* papers has been published. A significant change and further improvement in this service has been made and is taking effect as this report is issued in June, in that the previous very short abstract, often merely descriptive of the paper rather than revealing its subject matter, has been developed into a digest article, each of which with one or two illustrations will comprise units of approximately one page in *ELECTRICAL ENGINEERING*. Thus, every reader is assured of receiving at least the essential substance of every technical program paper.

The physical appearance of *ELECTRICAL ENGINEERING* was improved beginning with the September 1947 issue by a change to white paper of better quality, and a further change was made in the finish of the new paper to improve the quality of printing, effective with the April 1948 issue. In general, the paper supply situation has eased to a point where, with the close co-operation of suppliers, it has been possible to meet the steadily growing requirements for *ELECTRICAL ENGINEERING* paper. Severe production delays and other related difficulties incidental to protracted printing congestion have continued, with resulting tardiness in the issuance of the monthly edition. This has been annoying to readers and to advertisers, and has compounded the production difficulties of the editorial staff. As this report is issued, however, substantial improvement is being made at long last in the printing schedule, and it seems reasonable to hope that *ELECTRICAL ENGINEERING*'s mailing schedule of the 22d of the month preceding the date of issue can be re-established with the May 1948 issue.

ELECTRICAL ENGINEERING's circulation of the April 1948 issue was 45,000, one of the highest in the whole field of technical and professional journals. The steady growth of this circulation during recent years is indicated in the following:

January 1948	43,000
January 1947	36,300
January 1946	29,000
January 1945	26,450
January 1944	25,000
January 1943	24,900
January 1942	24,800
January 1941	23,900
January 1940	23,000
January 1935	18,500

The number of pages of published material for a few years back is shown in Table I, and there is a very large increase in 1947. This increase is a reflection, to a degree, of the additional number of general meetings, and of the effort to supply the readers' demands.

Advertising. Advertising policies and procedures have been given special attention by the publication committee and by the board of directors during the year. A more active advertising program was adopted, a new advertising director joined the publication staff at headquarters, advertising field representatives located in Chicago were engaged to service the Middle West territory, and advertising space rates approximately were doubled to bring them to a level more nearly commensurate with *ELECTRICAL ENGINEERING*'s production costs and high level of circulation. Also, advertising agency commissions were established, in accordance with prevailing trade practice. The resultant effect of these changes, while not fully felt to date, was to arrest the definite and alarming downward trend in advertising revenue, and actually did produce an increase in 1947–48 over 1946–47. Further effect is expected in 1948–49.

An interesting and useful 4-page pamphlet was prepared, entitled, "Industry Depends on the Electrical Engineer," which shows the geographical paid distribution and occupational analysis of readers for 1947 for 40,847 circulation of *ELECTRICAL ENGINEERING*. These data will be of great use and help in our advertising campaign.

PROCEEDINGS. Under the new publication program, the *PROCEEDINGS* was established expressly for the purpose of providing a special interim service for those members especially interested in the early availability of the full and complete technical and mathematical details of subject matter within the fields of their particular professional interests, including the discussions of the papers presented. In effect, the *PROCEEDINGS* is a preprint service from the annual *TRANSACTIONS* through which members may receive *TRANSACTIONS* papers and discussions many months ahead of their best possible availability in the *TRANSACTIONS* volumes. The *PROCEEDINGS* sections are made available to the membership through the medium of order forms which are published from time to time in *ELECTRICAL ENGINEERING* as the material covered is about to become available for distribution. The 1947 *TRANSACTIONS* papers were serviced by order forms published in the February, June, and December 1947 issues of *ELECTRICAL ENGINEERING*, and the first year's experience indicates that about 2,000 members are availing themselves of this *PROCEEDINGS* service. In brief, the governing policy is that single copies of *PROCEEDINGS* sections may be obtained by any member through the use of the order form, up to ten per cent of the items on any order form free and additional items at a flat subsidized price of ten cents each if remittance accompanies the order; Student members may procure desired *PROCEEDINGS* sections at ten cents each through the use of the order form. So far as it has been possible to determine, this is a pioneer effort in such selective distribution of diversified publications. It is working out quite closely in accordance with expectations, except that the quantity requirements are substantially lower than were contemplated. Except for the production and distribution delays resulting from printing congestion, which have been quite severe, the membership reaction to this specialized service has been strongly favorable.

As the first year of this service came to a close, the printing-production delays were being substantially overcome, with the result that a corresponding reduction has been possible in the time delay between receipt of a member's order form and the mailing of the requested *PROCEEDINGS*. However, there is a minimum delay below which it is impossible to go. This minimum delay is dictated by two factors: first, the fact that the final processing of a technical paper manuscript cannot even be started until about a month after the date of a meeting at which the paper is presented, when the meeting discussion on that paper becomes available; second, the fact that in the interests of production economy it is necessary to equalize over a 12-month period the peak load influxes of new material related to the various District and general meetings.

In accordance with the action taken by the board of directors at its November 1947 meeting, the general governing policy has been established that the primary objective of the *PROCEEDINGS* is the immediate distribution of requested technical material in minimum possible time after publication of an order form in *ELECTRICAL ENGINEERING;* that such order forms will be honored subsequently within the limits of available printed stock of *PROCEEDINGS*, that no effort will be made to maintain stocks of *PROCEEDINGS* available for distribution after the issuance of the *TRANSACTIONS* volume containing the material from those *PROCEEDINGS*.

Preprints. The practice of issuing technical program papers in pamphlet form (preprints) in advance of the meetings for which they are scheduled is being continued in line with the general publication policy. Some changes in procedural details have been made, however, in line with the recommendations of the finance committee as embraced in the budget action of the board of directors at its November 1947 meeting: first, free distribution is limited to a maximum of five copies of a paper to go to each author, and not to exceed ten copies of each paper to be available to the sponsoring technical committee expressly for the purpose of inviting and stimulating specific discussions; second, the establishment of a flat member price of 30 cents per copy (nonmembers 60 cents) regardless of the number of pages and

regardless of whether procured by mail or personally at headquarters or meetings; third, the provision that authors or the companies with which they are affiliated may procure, by written order submitted with the final draft of the manuscript, nominal quantities at the individual member price of 30 cents per copy in advance of the meetings, leaving large quantity orders to be serviced and quoted upon separately by headquarters after the initial requirements for the meeting have been satisfied. This latter restriction was essential to reduce and avoid critical premeeting congestion in the printing and distribution of this material.

Special Publications. Several special publications were produced during the year and made available for sale in accordance with announcements carried in *ELECTRICAL ENGINEERING*. These include:

1. "Application of Electric Conversion Equipment to the Electrochemical Industry"
2. "Bibliography on High-Frequency Dielectric Heating"
3. "Electric Power Applications in the Textile Industry"
4. "Electric Power in the Pulp and Paper Industry"
5. "Statistical Methods in Quality Control"
6. "Tidd 500-Kv Test Lines"
7. "Report on Electron Tube Survey of Instrument Manufacturers and Laboratories"

Year Book. Effective with the 1948 edition, the "Year Book" will be published and distributed in the early fall, probably during September, instead of early in the spring as it has been in recent years. The objective of this change is to make available at the beginning of the AIEE administrative year, an up-to-date roster that will be of maximum service to the new officer and committee personnel and other members. This change was recommended by the publication committee on the basis of a questionnaire survey, and approved by the board of directors at its January 1948 meeting.

The 1948 "Year Book" also will include the 1947–48 officers, so that there will be no break in the publishing of these lists.

Administrative. In addition to the very heavy production load picked up by the editorial staff incidental to the activation of the enlarged publication program in 1947, there was the attendant difficulty of procuring and retaining the necessary qualified personnel. Typical of prevailing economic and industrial conditions, the turnover in editorial personnel for the year was exceedingly high, the equivalent of 100 per cent staff turnover in terms of the number of persons involved, and the losses in training and production time. This condition seriously impeded operations and materially reduced production efficiency. However, toward the close of the fiscal year covered by this report, appreciable improvement had been accomplished and the situation seemed to be approaching a degree of reasonable stability in terms of qualified personnel, and the substantial assistance of the finance committee and the board of directors toward the accomplishment of this condition hereby is appreciated and acknowledged.

Costs. The cost of publishing *ELECTRICAL ENGINEERING* and associated printed matter materially has increased. This is due to a number of factors, among which are

1. Paper cost.
2. Printing cost.
3. Additional pages in *ELECTRICAL ENGINEERING*, additional preprints, and additional *PROCEEDINGS* as a result of a third general meeting.
4. Editorial staff payroll increases.
5. Increased circulation.

Of course, there are a number of revenue increases to compensate to some extent for these cost increases, and among these are increased advertising rates, increased charges for preprints, and so forth.

SECTIONS COMMITTEE

1. Section Activities. Section activities have continued at accelerated postwar levels, as indicated in the following:

New Sections organized

Arkansas
Arrowhead, Minn.
Canton, Ohio
Panhandle Plains, Tex.
Rock River Valley, Ill.
Shreveport, La.

New Subsections organized

Binghamton Area, N. Y.
Corpus Christi, Tex.
Fox River Valley, Wis.
Freeport, Tex.
Johnstown, Pa.
Little Falls, Minn.
Quad Cities, Iowa
Red River Valley, Minn.
St. Maurice Valley, Quebec, Canada
San Jose, Calif.
San Juan, Puerto Rico
Shasta, Calif.
West Central Texas
Western Virginia

The Sections committee actively has encouraged aggressive Section programs and has cosponsored Section membership promotion meetings.

The booklet "Section Activities," prepared by last year's Sections committee, has been circulated to Section officers and all other interested Institute officers and committeemen. This excellent work covers all essential Section activities.

During the current season, an outline of model Section activities has been perfected under the direction of Fischer Black, Sections committee member from the Washington Section. This will assist materially new Section officers as a guide and check sheet for each item of successful Section operations. This outline will be presented in its final form at the Sections committee meeting in Mexico City.

2. Bylaws Changes. Several amendments to the Institute bylaws have been recommended by this year's Sections committee. One of these formalizes the recognition of Subsections and Section Divisions; another, increases from 25 to 50 the number of members required on a petition for Section status. This recommendation is based on the fact that practically all Sections authorized within recent years have developed from Subsections or Section Divisions and the fact that it is not economically sound to have Sections of the Institute with less than 50 members.

Another recommendation for a change in bylaws proposes that any Section having less than 30 members or holding less than four meetings during a fiscal year will be changed to a Subsection at the end of the following year if during the interim period sufficient activity has not been exhibited to conform with the foregoing requirements.

3. Subcommittees on Prize Paper Rules and Section Finances. A subcommittee of the Sections committee, under the chairmanship of Fischer Black, has been appointed to study and make recommendations for changes in the Institute prize paper rules. This subcommittee is expected to report at the meeting of the Sections committee in Mexico City.

Another subcommittee of the Sections committee, under the chairmanship of Victor Siegfried of the Worcester Section, has been appointed to study the subject of "Section Finances." This committee likewise will report at the Mexico City meeting.

4. Section Committee Meetings. The Sections committee held a meeting in Pittsburgh on January 28, 1948, with 55 members and guests in attendance. Actions at this meeting included recommendations to the board of directors for the assignment of Alaska to the Seattle Section and District number 9, in addition to the recommended changes in bylaws and other actions mentioned in other parts of this report.

The Sections committee will conduct a conference on Section operation and management, and a delegates' get-together during the summer general meeting in Mexico City.

5. Co-operation With Other Institute Committees. The Sections committee has maintained active co-operation with the Institute committees on membership transfers, Student Branches, and technical program, the industry co-ordinating committee, the general industry applications committee, the industrial power system subcommittee, and to a lesser degree many other working committees of the Institute.

COMMITTEE ON RESEARCH

The principal activity of the committee during the past year was the sponsoring of a symposium on new materials at the winter

Table II. Section and Branch Statistics

	For Fiscal Year Ending April 30					
	1943	1944	1945	1946	1947	1948
Sections						
Number of Sections	73	73	73	75	75	81
Number of meetings held	598	738	884	1,210	1,333	1,340
Total attendance	66,111	83,120	96,346	113,531	155,649	109,637
Branches						
Number of Branches	125	125	125	125	126	127
Number of meetings held	942	755	547	716	1,018	1,172
Total attendance	38,227	24,768	17,132	22,844	40,669	77,040

general meeting. A total of 14 papers was presented, principally in the fields of magnetic materials, dielectrics, insulations, and semiconductors.

The papers brought out a considerable amount of new information which may influence future trends in electrical engineering practice to a significant degree.

A summary of the properties of the materials discussed also was prepared for the proceedings to make the significant parts of the information readily available.

Proposed papers for the summer general meeting were reviewed.

A former activity of this committee, namely, the listing of suggested research projects for which it is deemed there is considerable need is again under consideration by the committee as a possible future activity.

STANDARDS COMMITTEE

During the early months of the year 1947–48, the Standards committee approved a large number of Standards and codes, some of which had been in course of development for long periods, because of wartime activity demands on the time of many of those making up the originating committees. Since the June 1947 meeting, a large part of the committee's time has been devoted to the reorganization and reactivation of committees whose personnel had been affected by changes in member business affiliations following the war.

One of the projects on which formal approval was obtained, was the report of the special committee on sign of reactive power under R. C. Bergvall, as chairman. For years this question has been the subject of controversy among the various interested groups. After a careful study of all the factors involved, a utilization of all available publicity in obtaining criticisms and suggestions, formal action reversing the present AIEE Standards was taken, that is, a change of the sign of reactive power from minus to plus.

The subcommittee on applications of statistical methods to quality control, under Chairman Dobson, having arranged for the presentation and publication of 11 papers on that subject, now is negotiating with the American Society for Quality Control, for the obtaining of additional papers on application in the electrical field as well as the institution of an educational campaign toward the wider application of quality control.

The prospect for future Standards activities seems to have been enhanced by the recent enlargement of the Institute technical committee structure. The greatly increased number of technical committees certainly should result in greater interest and activity in each individual field. A resumption of activity in the International Standards field also can be expected, due to the recent organization of the International Organization for Standardization. The United States National Committee of the IEC, while inactive during the war, again will take over the handling of the American phase of the ISO work. With relation to this international phase of Standards work, attention is called to the successful international session held under the auspices of the Standards committee in Montreal on June 10, 1947, and the proposed session in Mexico City during the summer general meeting.

The work on aeronautical electrical Standards is going ahead with the active cooperation of the technical committee on air transportation under S. D. Summers, chairman. Direct contact now has been made with the chairman of the National Aeronautical Standards Committee, which it is hoped will lead to a closer contact than has been had in the past with those interested in the furtherance of such standardization.

During the past year the following Standards and codes have been approved by the Standards committee: General Principles Upon Which Temperature Limits Are Based in the Rating of Electric Machines and Apparatus (a revision); Electric Recording Instruments; Neutral Grounding Devices (a revision); Aircraft D-C Apparatus Voltage Ratings; Master Test Code for Resistance Measurements (a report); Letter Symbols for Physics; Potheads; Roof, Floor and Wall Bushings; Graphical Symbols and Abbreviations for Use on Drawings; Graphical Symbols for Electron Devices.

The sectional committee on definitions of electrical terms, for which the Institute is sponsor, a major AIEE project, inactive during the war, now has been reorganized completely under the chairmanship of Professor Dawes. The work of its 18 subcommittees is now well under way.

In a move to expedite both the approval of Standards and codes, and the appointment of AIEE representatives on various standardizing committees, Chairman Jones made an appeal to the board of directors for the delegation to the Standards committee of the power to give such approval in the name of the Institute. The board of directors approved his request on April 23, 1948.

TECHNICAL PROGRAM COMMITTEE

Technical Programs. The technical programs for the general meetings and District meetings have been kept on a high standard and at the same time enlarged to correspond to the growth in Institute membership. In Table III, the number of papers presented at each of the seven meetings which were held during the year are given, together with the attendance. The total of 233 *TRANSACTIONS* papers and 30 advance copy papers approaches closely the 270 average estimated number of papers a year for the five year period following 1945 presented in the 62d annual report of the board of directors for the fiscal year ending April 30, 1946.

Planning for Programs. In the arrangement of programs, an attempt has been made to cover all new developments in the field of electrical engineering and the allied arts. The geographical location of meetings also has been taken into consideration, and papers have been presented which were of particular interest in certain regions. For the summer general meeting in Montreal, sessions were held on power supply in paper mills and hydroelectric power generation. For the Pacific general meeting in San Diego, an important symposium on sonar was held as well as two sessions on aircraft. For the Middle Eastern District meeting in Dayton, the home of Wright Field and the heart of small motor manufacture, it was only natural that the program was devoted to electrical applications to aircraft and the design and performance of small motors. For the winter general meeting in Pittsburgh, a number of sessions were held which dealt with electrical applications to the production of iron and steel as well as mining.

Analysis of Subject Matter. In the field of power, two important symposiums on the Tidd 500-kv test lines were presented at the Midwest general meeting. The papers described the apparatus, line conductors, insulators, and hardware design for these test lines, and the resulting data will be of value in designing higher voltage transmission lines. In the field of communication, sessions on frequency and pulse modulation, sonar, and synthetic crystals were outstanding. In the industrial field, sessions on applications to the steel industry and the related control problems were significant contributions. In the field of general applications, the subjects of light traction, heavy traction, and the economics of power supply, as well as developments in Diesel electric locomotives have been well covered.

In Table IV, the papers have been classified on the basis of the technical committee structure in the Institute. Papers jointly sponsored have been indicated by half credits respectively. This is only one of several possible bases for the classification of papers. For example, if all of the papers which dealt with electronic applications, such as in the fields of electronic power converters, industrial control, electric heating, communication, and instruments and measurements were classified under electronics, there would be a great many more electronics papers. A number of valuable papers filling a definite need have been brought forth in the fields of electronic instruments, servomechanisms, and the heat pump. There also has been an increase in the number of communication papers presented during the year.

Midwest General Meeting. The meeting held in Chicago, November 3–7, inaugurated the first of the Midwest general meetings designed to fill a need in that section of the

Table III. Number of Papers Presented During the Year Ending April 30, 1948*

Meeting	District	Advance Copy Only	Conference Papers	Transactions	Total	Attendance
Summer, Montreal		7	39	54	119	1,451
Pacific Coast, San Diego			20	16	36	589
Middle Eastern, Dayton	12	7		21	40	502
Midwest, Chicago		1	30	29	60	1,449
Winter, Pittsburgh		12	118	87	217	3,081
Great Lakes, Des Moines	8	3		10	21	354
North Eastern, New Haven	24		7	16	47	669
	44	30	214	233	540	8,095

* Repeated presentations not included.

Table IV. Classification of Papers for Year Ending April 30, 1948

	Summer Montreal	Pacific Coast San Diego	Middle Eastern Dayton	Midwest Chicago	Winter Pittsburgh	Great Lakes Des Moines	North Eastern New Haven	Total	Per Cent
Communication and Science Group									
Basic sciences				3.0	6.0	1.5		10.5	
Communication	8.0	2.0	0.5	3.0	14.0	0.5		28.0	
Instruments and measurements	5.5	3.0	2.0		11.5	1.5	2.0	25.5	
Electronics	1.0		0.5		5.5	1.5	1.0	9.5	
Nucleonics									
Application of electricity to therapeutics	1.0				1.0			2.0	
									28.7
General Applications Group									
Air transportation		4.0	8.0					12.0	
Domestic and commercial application					2.0			2.0	
Land transportation	5.0				11.0		0.5	16.5	
Production and application of light			0.5					0.5	
Marine transportation	1.5							1.5	
									12.4
Industry Group									
Chemical, electrochem, and electrometallurgical				0.5	1.0	0.5		2.0	
Electric heating	3.0				1.0			4.0	
Electric welding									
General industry application	4.0							4.0	
Industrial control	4.0	0.5		1.0	3.5			9.0	
Industrial power systems	1.0			1.0	3.0			5.0	
Mining and metal industry		1.0			2.0			3.0	
									10.3
Power Group									
Carrier current	2.0				0.5			2.5	
Electronic power converter					0.5		0.5	1.0	
Insulated conductor					5.0		1.0	6.0	
Power generation	4.0	1.5		3.0	8.5	1.0		18.0	
Protective devices	1.0			1.0	2.0			4.0	
Relay	1.5				2.0		1.0	4.5	
Rotating machinery	5.0	0.5	16.0	5.0	5.0	2.0	4.5	38.0	
Substation	0.5				1.0			1.5	
Switchgear	5.0	1.0		1.5	5.0	3.0	3.5	19.0	
System engineering	0.5			2.0	0.5	0.5		3.5	
Transformer	2.5			2.0	2.5		1.5	8.5	
Transmission and distribution	5.0	2.5	0.5	7.0	5.0	1.0	0.5	21.5	
									48.6
Totals	61	16	28	30	99	13	16	263	

country and relieve the congested winter general meetings. Sixteen technical sessions and conferences were held, one of which was joint with the National Electronics Conference, and three general sessions. The meeting was very successful with a total registration of 1,449, and for the future indications are that the programs for this meeting will be larger. For the 1948 Midwest general meeting to be held in Milwaukee, 35 technical sessions and conferences already have been scheduled.

Future Meetings. The program for the summer general meeting in Mexico City has been completed, and the programs for the Pacific general meeting and the Midwest general meeting are well under way. Several sessions already are in prospect for the 1949 winter general meeting in New York.

Administration Group Committees

BOARD OF EXAMINERS

Statistics relating to the number of cases handled by the board of examiners during the year 1947–48 are given in Table V. The current total represents a slight increase in the record figures for 1946–47, or about 72 per cent more than the 1945–46 total. These figures are exclusive of those covering "Enrolled Students" which rose from 3,931 to 5,894, or about 50 per cent.

In the past the examiners frequently have emphasized the need for a more careful preparation of all applications, both for admission and transfer. The results of these pleas are evident in a considerable decrease in the number of transfers held up by the need for additional information. The examiners wish therefore to express their appreciation for the excellent work carried on by the Section transfer committees aiding in this connection. With regard to applications for direct admission, the examiners still find it necessary to ask frequently for more detailed statements of record and additional supporting references. A bare statement of titles of positions held with dates is not sufficient to enable the board of examiners to arrive at a decision on eligibility. Likewise, the fact that an applicant has reached the lower age limit specified in the constitution is no proof of eligibility.

Among the hundreds of cases that have been passed upon by the board of examiners, particularly since the end of the war, there have been many from foreign countries. This number seems to be growing at an unusually rapid rate. The examiners therefore have expressed the opinion that this situation should be called to the board of directors' attention. As the dues paid by such members are determined through adjustments based on currency exchange rates, resulting in payments in many cases 40 per cent below the stated dues paid by engineers in the United States, it would seem that in accepting foreign applicants under such conditions we may be aiding in making more acute a recognized existing shortage of funds for fulfilling our obligations to the membership in the United States.

COMMITTEE ON CONSTITUTION AND BYLAWS

Submitted by the committee on October 10, 1947, and adopted by the board of directors on November 5, 1947, were the following recommendations.

1. **Constitution.** In sections 39, 40, 50, 59, changing the word "convention" to "general meeting."

2. **Bylaws.** In sections 6, 7, and 11, changing the words "United States or Canada" to "United States, Canada, or Mexico."

Section 39. For the word "national," substituting the word "international."

Section 65. Adding to the list of general committees the "committee on management."

Section 85. For the word "national," substituting the word "general."

Section 85. Deleting last paragraph to remove repetition.

3. **New Section.** Outlining the membership and the cope of the new committee on management.

Submitted by the committee on January 7, 1948, and adopted by the board of directors on January 29, 1948, were the following recommendations.

1. **Constitution.** In section 10 requiring proposals for transfer to grade of Fellow to originate from "five Fellows," rather than from "five Members or Fellows." Section 20. Deleting entire section, to do away with "Life Membership."

Section 32. Changing the mailing date of election ballots from "the first week in April" to "April 15."

Section 35. Redefining the membership of the board of directors to read: "The board of directors shall consist of the president, the two surviving past presidents most recently retired, the vice-presidents, the directors, and the treasurer."

Section 44. Changing the requirement that "each standing committee shall include at least one member of the board of directors" to requiring that "each co-ordinating committee shall include at least one member of the board of directors."

Also, transferring this clause to section 65 of the bylaws.

2. **Bylaws.** In sections 6, 7, 22, and 63, deleting the words ("Journal of A.I.E.E.").

Section 21. Deleting the heading "Life Membership" and the table in section 22, headed "Life Membership Rates." Also, rewording section 21 to take care of present "Life Members."

Section 23. Changing the mailing date of election ballots from "the first week in April" to "April 15."

Section 33. Making optional the appointment of a District co-ordinating committee, and specifying that if appointed, such a committee "shall function continuously in an executive capacity for the District executive committee."

Section 35. Providing that a District meeting committee shall be "composed of members within the District, preferably located in the area where the meeting is to be held."

Section 35. Deleting the word "national" before the words "technical program committee."

Section 35. Changing the world "national" to "general."

Section 65. Under the heading "General Committees," adding the committees on education, management, Members-for-Life fund, Institute publicity, and transfers.

Section 65. Under the heading "Technical Committees," adding the new "committee on computing devices," and rearranging all the technical committees under their proper co-ordinating committees.

Table V. Application for Admission and Transfer, 1947–1948

Applications for Admission			
Recommended for grade of Associate	2,160		
Re-elected to the grade of Associate	90		
Not recommended	1		
		2,251	
Recommended for grade of Member	297		
Re-elected to the grade of Member	11		
Not recommended	85		
		393	
Recommended for grade of Fellow	6		
Not recommended	1		
		7	
Applications for Transfer			
Recommended for grade of Member	472		
Not recommended	32		
		504	
Recommended for grade of Fellow	86		
Not recommended	1		
		87	
Students			
Recommended for enrollment as Students			5,894
Total			9,136

Section 68. Adding to the technical program committee the chairmen of the technical co-ordinating committees, and the chairman of each of the following committees: education and research.

3. **New Section.** Defining the scope of the transfers committee.

Section 70. Adding to the planning and co-ordination committee the chairman of each co-ordinating committee.

Section 97. Clarifying the procedure relative to revisions of technical papers, before publication by the Institute.

The following recommendations of the committee were adopted by the board of directors on April 23.

1. **Bylaws.** Section 22. Defining the "suggestion to be considered by the nominating committee" to be those "from the general membership."

Section 32. Adding to the District executive committee a member of the Sections committee who is resident in the District.

Section 36. Changing the minimum number of members, who may petition for the formation of a Section from 25 to 50.

HEADQUARTERS COMMITTEE

As requested by the board of directors, the committee considered a proposal that the main lobby of the Engineering Societies Building be converted into a common board room, a suggestion which had been under consideration by the other founder societies and the United Engineering Trustees, Inc. The committee recommended that the proposal be not endorsed, as the amount of use of the AIEE board room precludes its discontinuance, the modification of the lobby was considered to be fundamentally undesirable, and the cost would not be justified by the benefits that would be derived.

The committee has kept in mind continuously the need for more office space in the building, and discussed various possibilities.

COMMITTEE ON INSTITUTE PUBLICITY

The committee on Institute publicity had as its primary task the handling of public relations for the winter general meeting held this year in Pittsburgh during the latter part of January. It was decided again to retain the services of a professional public relations counsel. Public Relations Research Service, Inc., of Pittsburgh was authorized to carry on this work, and it is believed that the assignment was completed in an effective manner.

Publicity for the summer general meeting in Mexico City is being handled by the local organization with assistance and suggestions from the general committee. Material on the over-all objectives and activities of the Institute has been sent to Mexico City, and the committee will continue to provide any other assistance that is required.

At the request of President Hull, the committee has formulated and submitted to the board of directors a draft of a statement of Institute publicity policy. The committee's suggestions recommend consideration of publicity as a continuing and integrated activity rather than a series of special efforts centered about a few important meetings.

At the same time, the committee submitted a tentative definition of its scope for inclusion in the bylaws.

COMMITTEE ON TRANSFERS

Continuing the work started last year by the committee on transfers, the effort again this year has been directed toward encouraging the Sections to maintain active transfers committees and toward providing all Sections with information on methods of operation which have been used with success as reported by individual Sections. To arrive at a best procedure to meet the needs of the greatest number of Sections has been the aim of the committee on transfers.

At the start of this fiscal year, the Institute made the committee on transfers a standing committee. This action aided materially in encouraging the Sections to establish their own transfers committees also as standing committees. Practically all Sections now actively and continuously are urging their qualified members to apply for high grades of membership. The necessary qualifications for each grade are more uniformly and generally understood.

The committee on transfers reported during the Sections committee meeting in Pittsburgh in January as part of the presentation, "The Model Section." That report said, in part:

> The activities of committees on transfers should not be of a campaign nature, in contrast with the work of membership committees. The work of transfers, consistently and continuously, year in and year out, should be to present the opportunity to qualified members to advance in grade of membership. . . . For the good of the individual and the welfare of the Institute it takes a group in each Section—"The Model Section"—to analyze its members and to encourage its qualified members to become what they should be in grade of membership. Our job is not to make members of higher grade but to present the benefits of the higher grades of membership to the extent that personal inertia is overcome. It is an honor to be an Associate. It is a higher honor to be a Member. It is the highest honor to be a Fellow of the AIEE.

On March 22, the committee on transfers and the Sections committee jointly sent a questionnaire to all Sections requesting pertinent information on their activities and experiences in transfers work. Reports on their most successful techniques also have been requested. Replies are coming in and will be correlated in a final report to be turned over to the succeeding committee on transfers of the Institute. If this report results in a generally acceptable and enduring plan of action for continuing grade transfers with minimum effort in the Sections, the primary aim of this committee will have been accomplished.

MEMBERSHIP COMMITTEE

In evidence of the active work carried on by Section membership committees during the past year, the total membership of the Institute increased to 28,400 on April 30, 1948—1,922 more members than enrolled for the same time last year. Table VI of membership statistics shows the nature of the changes in membership during the year. Mention should be made that the losses include 200 cases of nonresident members for whom inactive status had been maintained throughout the war period, who have not found it possible to become reinstated.

Applications for admission to membership received during the year totaled 3,753, a record figure for the Institute. Many of the applications still are pending, and all indications point to a steady growth in the membership total.

At the end of April 1947, 1,090 members were in arrears for dues, whereas this year the number had increased to 1,288. However, the increase amounts to less than one-half of one per cent of the membership total, and therefore is not significant.

The phenomenal growth in Student membership is shown in Table VIII, 13,917 persons being so enrolled with the Institute on April 30, 1948. From this source there should be a substantial increase in the membership of the Institute during the next few years.

The membership committee continued to encourage decentralization of its operations and increased activity on the part of the District vice-chairmen. At the beginning of the fiscal year, a recommended procedure of operation was sent out to all the District vice-chairmen for their guidance in handling membership activities in their Districts.

A small executive subcommittee of the membership committee was established. This subcommittee met at intervals of two months during the year and handled the general operational problems of the membership committee.

A membership committee meeting was held in Pittsburgh at the winter general meeting. At this committee meeting, presentations were made on the various problems connected with stimulating membership. This committee meeting was attended by a number of members of the membership committee and executives of the AIEE. Copies of the presentations were sent to all members of the membership committee so as to disseminate the new ideas which were presented.

A very active program was followed with all of the Sections to promote the advancement from Student to Associate grade. This program was very successful and the number of applications from Students this year exceeded all previous records. The membership committee continued its program of co-operation with Student Branches, and members of the committee presented talks on the benefit of AIEE membership before many of the Student Branches.

A subcommittee studying an alternative method of advancing Students to Associate membership continued to work with the committee on Student Branches in an effort to work out an improved method. It is planned to attach an application for the grade of Associate to the bill for Student dues which is sent to graduating seniors. In the letter transmitting this bill, the

Table VI. Membership Statistics for Fiscal Year Ending April 30, 1948

	Honorary Members	Fellows	Members	6-Year Associate	Associate	Subtotals	Totals
Membership							
April 30, 1947	9	1,039	7,500	7,652	10,278		26,478
Additions							
New members qualified		3	281		2,478	2,762	
Former members reinstated or re-elected		7	110	76	120	313	
Subtotals		10	391	76	2,598	3,075	
Transfers		80	511	1,206		1,797	
Totals		90	902	1,282	2,598	4,872	
Deductions							
Died	1	34	75	59	25	194	
Resigned		2	45	101	118	266	
Dropped		5	86	251	351	693	
Subtotals	1	41	206	411	494	1,153	
Transfers			79	417	1,301	1,797	
Totals	1	41	285	828	1,795	2,950	
Net Changes	−1	49	617	454	803	1,922	
Membership April 30, 1948	8	1,088	8,117	8,106	11,081		28,400

Table VII. Number of Applications Received From Student Members and From All Others

Year Ending April 30	Students	All Others	Total
1948	1,481	2,272	3,753
1947	938	2,331	3,269
1946	308	2,453	2,761
1945	249	2,179	2,428
1944	466	1,908	2,374
1943	783	1,431	2,214

Table VIII. Number of Student Members as of April 30

Year	New Applications	Renewals	Total
1948	7,876	6,041	13,917
1947	5,092	3,929	9,021
1946	2,574	2,513	5,087
1945	2,326	2,287	4,613
1944	2,242	2,656	4,898
1943	2,512	3,200	5,712

Table IX. Record of AIEE Membership

Year	Total May 1	Year	Total May 1	Year	Total May 1
1884	71	1906	3,870	1927	18,344
1885	209	1907	4,521	1928	18,265
1886	250	1908	5,674	1929	18,133
1887	314	1909	6,400	1930	18,003
1889	333	1910	6,681	1931	18,334
1890	427	1911	7,117	1932	17,550
1891	541	1912	7,459	1933	17,019
1892	615	1913	7,654	1934	15,230
1893	673	1914	7,876	1935	14,269
1894	800	1915	8,054	1936	14,600
1895	944	1916	8,202	1937	15,308
1896	1,035	1917	8,710	1938	16,078
1897	1,073	1918	9,282	1939	16,605
1898	1,098	1919	10,352	1940	17,213
1899	1,133	1920	11,345	1941	17,886
1900	1,183	1921	13,215	1942	18,944
1901	1,260	1922	14,263	1943	20,161
1902	1,549	1923	15,298	1944	21,407
1903	2,229	1924	16,455	1945	23,072
1904	3,027	1925	17,319	1946	25,090
1905	3,460	1926	18,158	1947	26,478
				1948	28,400

seniors will be invited to apply for Associate membership immediately, and the processing of such applications will be delayed so that final action is taken at the time Student membership is about to expire.

A revised edition of a "Section Membership Committee Manual," was completed and sent out to all the Sections at the beginning of the fiscal year.

It is recommended that the membership committee in the future be made up of a chairman, vice-chairman, ten District vice-chairmen, and 13 members-at-large. The District vice-chairmen should serve a period of two years, with half of the group changing each year. Ten of the members-at-large should be former District vice-chairmen, and half of this group changed each year. The other three members-at-large should be liaison members from the board of directors, the committee on Student Branches, and the Sections committee. If this plan is followed in appointing the membership committee each year, the Institute will have the benefit of a maximum amount of experience on the committee, and be assured of a continuity of carrying out membership committee policy.

It also is recommended that appointees to the office of District vice-chairman have Section membership background.

Professional Group Committees

PROFESSIONAL GROUP CO-ORDINATING COMMITTEE

The committee co-operated with President Hull in selecting members for the several committees in the professional group.

A start was made toward closer co-ordination of the work of the committee on Student Branches and the committee on education. The problem of consolidation or closer co-ordination still is being studied and several joint activities are being considered.

Considerable correspondence was carried on between chairmen of the committees within the professional group regarding the proposal that the Institute join the co-operating organizations of the National Security Committee. A recommendation was agreed upon and submitted to the board of directors. This recommendation, which was adopted by the directors, was to the effect that, first, the Institute cannot take a stand on the subject of universal military training and, therefore, postpones the appointment of a representative to the National Security Committee, and, second, that the Institute is willing to work with any organization in the preparation of a training program to the end that the most effective use of technically-trained personnel is made.

COMMITTEE ON EDUCATION

A session was sponsored at the summer general meeting, with four educational papers. The committee will present a session at the 1948 summer general meeting featuring three papers, and, in co-operation with the committee on Student Branches, plans a joint session at the Midwest general meeting on the general theme of the electrical engineering profession. The intent of this meeting is to present sound information to the student membership concerning the profession.

Through the efforts of this committee, a plan for the publication of a "Bibliography of the History of Electrical Engineering," has been evolved. This material has been gathered by Professor T. J. Higgins over a period of many years, and represents a worthy addition to the history of electrical engineering.

Two members of the committee have been appointed to co-operate with the committee on Student Branches in drafting a program of common interest. This subcommittee is urged to study such topics as licensing of engineers, greater co-ordination between the various branches of engineering and professional aspects of engineering as a career, and any other such topics particularly applicable to the student engineer.

COMMITTEE ON STUDENT BRANCHES

A considerable number of inquiries have been processed concerning the operation of joint Student Branches, and requests were received for the establishment of new AIEE Student Branches. A new branch was established at Wayne University in Detroit during the past year, and a recommendation has been made to the board of directors for the establishment of one additional Branch outside of the continental limits of the United States.

At the meeting in Montreal, in June 1947, there was an extensive discussion of the criteria for the establishment of new Student Branches. The discussion centered primarily around the interpretation of the phrase, in section 64 in the bylaws, which reads "an established engineering society in a college or technical school of recognized standing may, upon application of its officers and a member of the Institute . . . . . become associated with the Institute." It was the consensus of opinion of those at this meeting that the term "recognized standing" should be defined as Engineering Council for Professional Development accrediting in so far as the establishment of new Student Branches was concerned, because the activities of ECPD are supported by the AIEE, and there seemed to be no other clear-cut criteria on which recommendations could be based. It was decided that ECPD accrediting should apply in the United States, and that outside, where ECPD accrediting is not in effect, an approximately equivalent standard would be required.

Work was carried on by a joint subcommittee between the membership committee

Table X. Deaths of AIEE Members Reported in *ELECTRICAL ENGINEERING*

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in *ELECTRICAL ENGINEERING*
Allcock, Harold J.	M '13		M	Jan. '48, p. 103
Andrus, Lucius A.	A '03		F	Mar. '48, p. 301
Atkinson, Albert A.	A '07	Jan. 20, '47	M	May '47, p. 507
Baldwin, Edward A.	A '07	Sept. 27, '47	A	Dec. '47, p. 1256
Barrett, William R.	A '22	Sept. 22, '47	A	Dec. '47, p. 1256
Benjamin, George R.	A '07	April 28, '47	A	June '47, p. 617
Berger, David	A '28		M	Feb. '48, p. 200
Berry, Henry P.	A '26	April 22, '47	M	Nov. '47, p. 1151
Biddle, James G.	A '96		A	Mar. '48, p. 301
Biegler, Philip S.	A '07		F	Mar. '48, p. 301
Bostwick, Thomas J.	A '18	May 8, '47	M	July '47, p. 711
Brooks, Morgan	A '90	April 23, '47	F	July '47, p. 711
Brown, Charles C.	A '22	May 18, '47	F	July '47, p. 712
Buchman, Richard P.	A '38	Mar. 24, '47	A	June '47, p. 618
Buckhold, John F.	A '46	Jan. 3, '48	A	April '48, p. 408
Bulloch, George E.	M '41		M	Mar. '48, p. 301
Chadwick, George A.	M '46	Oct. 1, '47	M	Dec. '47, p. 1257
Chesney, Cummings C.	A '94	Nov. 27, '47	F	Jan. '48, p. 102
Colby, Safford K.	A '03	Aug. 4, '47	A	Oct. '47, p. 1029
Coit, Norman H.	A '19	Sept. 3, '47	F	Nov. '47, p. 1152
Collins, William H.	M '46	June 3, '47	M	Nov. '47, p. 1151
Cowles, Joseph W.	A '02		M	Nov. '47, p. 1151
Crocker, Theodore D.	M '44	June 29, '47	M	Sept. '47, p. 934
Cummins, Alfred C.	A '11	July 10, '47	A	Sept. '47, p. 934
Deininger, Harry W.	A '12	April 18, '47	M	June '47, p. 617
Dennison, Boyd C.	A '06	Aug. 19, '47	M	Oct. '47, p. 1029
Dexter, Jr., Howard W.	A '26		M	Mar. '48, p. 302
Diemer-Hansen, Jens L.	A '07	April 24, '47	M	Aug. '47, p. 847
Doyle, Harold K.	M '38	Sept. 27, '47	F	Dec. '47, p. 1255
Elmer, William	A '90	May 6, '47	F	July '47, p. 712
Entz, Justus B.	A '90	June 8, '47	A	Aug. '47, p. 847
Evans, Robert D.	A '21	May 8, '47	F	July '47, p. 711
Faile, Edward H.	M '31	Feb. 21, '47	M	Dec. '47, p. 1256
Foley, Hiram S.	A '07		M	Feb. '48, p. 200
Foote, Benjamin R.	A '19		M	Nov. '47, p. 1152
Forster, Frank T.	A '16	May 11, '47	A	Oct. '47, p. 1030
Germain, Henry A.	A '25		A	Nov. '47, p. 1151
Goodale, Edgar C.	A '27	April 28, '47	M	July '47, p. 711
Graner, Ladislas P.	A '23	Sept. 24, '47	M	Nov. '47, p. 1152
Greves, George L.	A '15	Sept. 6, '47	M	Nov. '47, p. 1152
Harden, William H.	A '15		M	Feb. '48, p. 200
Havill, Owen A.	A '11		F	Feb. '48, p. 200
Hayes, Hammond V.	A '89	Mar. 22, '47	F	June '47, p. 617
Heath, James G.	A '37	April 6, '47	A	Nov. '47, p. 1151
Hewlett, Ernest F. J. H.	A '99	Jan. 22, '47	M	July '47, p. 711
Higbie, Henry H.	A '08	Aug. 3, '47	F	Nov. '47, p. 1151
Hirsch, John G.	M '23		M	Jan. '48, p. 104
Hobble, Arthur C.	A '03	Mar. 25, '47	M	June '47, p. 617
Hollister, Francis H.	A '08	Sept. 20, '47	F	Dec. '47, p. 1255
Huang, Theodore S.	M '37		M	Sept. '47, p. 934
Hull, Paul B.	A '39	Jan. 5, '47	A	May '47, p. 507
Jones, Thomas D.	A '31	May 24, '47	A	Aug. '47, p. 847
Karapetoff, Vladimir N.	A '03	Jan. 11, '48	F	Feb. '48, p. 199
Koiner, Cyrus W.	A '04	Sept. 29, '47	F	Dec. '47, p. 1256
Lowe, Robert E.	A '34	Dec. 15, '46	M	June '47, p. 617
Ludwick, William L.	A '42		M	Feb. '48, p. 199
MacMillan, Campbell	A '08	Jan. 24, '48	M	April '48, p. 408
Mason, Karl C.	A '19	Mar. 16, '47	A	June '47, p. 618
Maxwell, Alexander	F '37	Feb. 10, '48	F	April '48, p. 407
Meyerherm, Charles F.	A '14	July 27, '47	M	Sept. '47, p. 934
Mizer, Charles A.	A '03	Oct. 24, '47	A	Dec. '47, p. 1257
Morehouse, Lyman F.	M '16	May 1, '47	F	July '47, p. 711
Morice, John H.	A '12	Feb. 17, '47	A	Aug. '47, p. 847
Mullen, James J.	A '12	June 23, '47	F	Oct. '47, p. 1029
Munro, James C.	M '45		M	Nov. '47, p. 1152
Murmann, Frederick J.	A '07		M	Nov. '47, p. 1152
McCammon, Floyd F.	A '28	Sept. 20, '47	M	Dec. '47, p. 1256
McCurdy, Ralph G.	A '16		F	Mar. '48, p. 300
McGraw, James H.	A '01	Feb. 21, '48	A	April '48, p. 407
McHugh, Henry R.	A '45	April 19, '47	A	July '47, p. 712
Nash, Luther R.	A '03	Aug. 10, '47	M	Oct. '47, p. 1029
Neall, Newitt Jr.	A '03		F	Mar. '48, p. 301
Oesterreicher, Sandor I.	A '07	Oct. 31, '47	M	Jan. '48, p. 104
O'Sullivan, Frederick A.	A '19	May 18, '47	A	Aug. '47, p. 847
Page, Wilson K.	A '10	Aug. 6, '47	A	Jan. '48, p. 104
Palmer, Granville E.	F '36		F	Mar. '48, p. 301
Pearce, Sir Standen L.	A '08		F	Jan. '48, p. 103
Persio, Louis N.	A '34	Aug. 6, '47	A	Nov. '47, p. 1151
Pilkington, John H.	A '26	Nov. 10, '47	M	Jan. '48, p. 103
Plank, Heber E.	A '13	Jan. 10, '47	M	May '47, p. 507
Rau, Otto M.	A '03	April 3, '47	M	June '47, p. 618
Ready, Lester S.	A '15	April 9, '47	F	July '47, p. 711
Reid, William M.	A '20		A	July '47, p. 712
Rice, Edward Y.	A '13	Sept. 29, '47	A	Dec. '47, p. 1256
Richards, Samuel H.	M '39	Jan. 9, '47	M	May '47, p. 507
Richey, Robert R.	A '43	Mar. 31, '47	A	June '47, p. 617
Rubel, Walter G.	A '18	July 24, '47	M	Nov. '47, p. 1151
Sappington, Richard D.	A '19		A	Nov. '47, p. 1152
Scattergood, Ezra F.	A '08	Nov. 15, '47	F	Jan. '48, p. 103
Shay, John T.	A '18	April 28, '47	A	Aug. '47, p. 847
Sirrine, Joseph E.	A '05	Aug. 7, '47	M	Oct. '47, p. 1030
Slack, Roy C.	M '46		M	Jan. '48, p. 104
Smith, Joseph B.	A '94		A	Sept. '47, p. 934
Smith, Walter L.	A '23	June 28, '47	A	Sept. '47, p. 934
Soares, John S.	A '09		A	Feb. '48, p. 199
Stoller, Hugh M.	M '19	Nov. 17, '47	M	Jan. '48, p. 103
Storer, Norman W.	A '95	June 5, '47	F	Sept. '47, p. 934
Thomas, Raymond S.	A '17	April 18, '47	A	July '47, p. 712
Torrey, Carleton E.	A '05	Aug. 15, '47	A	Nov. '47, p. 1151
Truran, Walter W.	A '23	Mar. 7, '47	M	May '47, p. 507
Turnegano, Jr., Paul J.	A '45	Sept. 29, '47	A	Dec. '47, p. 1257
Van Sanford, Russell	M '31	Feb. 14, '47	M	June '47, p. 618
Van Veen, John	A '20	June 9, '47	M	Aug. '47, p. 847
Wagner, Herbert A.	A '98	Dec. 5, '47	M	Feb. '48, p. 199
Waring, Tracey D.	A '06	April 27, '47	M	June '47, p. 617
Waterman, Frank N.	A '93		F	Mar. '48, p. 301
Weber, Fred D.	A '09	April 5, '47	M	July '47, p. 712
Wellwood, Arthur R.	M '22	Mar. 5, '47	F	July '47, p. 710
Wheadon, Frederick W. H.	A '17		A	July '47, p. 712
White, Francis J.	A '02	Oct. 4, '47	M	Dec. '47, p. 1255
Wickenden, William E.	A '07	Sept. 1, '47	F	Oct. '47, p. 1029
Willson, Frank G.	A '05	July 29, '47	M	Oct. '47, p. 1029
Woodward, Mark R.	A '11		M	Nov. '47, p. 1151

and the committee on Student Branches, to determine what could be done to encourage more Student members to become Associates upon graduation. A suggested procedure was presented at the winter general meeting by those members of this subcommittee who represented the committee on Student Branches. This suggested procedure has been submitted to all Student Branch counselors for a vote of approval or disapproval. The vote on this shows approximately ten to one for approval. The suggested procedure put up for vote read as follows:

A student may apply for Associate membership shortly before graduation. This may be done through an application form given by the faculty counselor or by the faculty counselor and a member of the membership committee from the local Section. The usual three references are required. This application will be transmitted to headquarters by the counselor upon graduation of the student and will be processed in the usual manner. The dues will continue at the Student rate until the expiration of the time limit for Student membership even though the member is now of the Associate grade. This would require revision of article IV, section 19 of the constitution.

In the event of arrears, that is, the member does not pay his Associate dues at the expiration of the time limit for Student membership rate or within three months thereafter, he will be placed on the suspended list and receive no further publications. This would require revision of section 18 of bylaws.

The present procedure also will remain in force as an alternate method of transfer from Student status to Associate grade. (Note this procedure at this time is only a proposal from the committee on Student Branches which at a later date may be forwarded to the board of directors. It is not now effective.)

The committee on Student Branches is assisting in re-editing the booklet "The Electrical Engineer" which is intended to orient and to guide high school students who are contemplating a career in electrical engineering.

The committee on Student Branches expects to enter into a co-operative arrangement with the committee on education, to sponsor a session at the Midwest general meeting in Milwaukee in October.

SPECIAL COMMITTEE TO REVIEW PRACTICES OF INSTITUTE PRIZE AWARDS

The draft of the proposed revised practice covering award of Institute prizes was reviewed with the board of directors and at the conference of vice-presidents and District secretaries in Montreal, in June 1947. After further revisions, the proposal was submitted to the board of directors and approved at the August 1947 meeting, at which time the special committee was discharged. The revised practices appeared in the December 1947, issue of *ELECTRICAL ENGINEERING*, and became effective January 1, 1948.

UNITED STATES NATIONAL COMMITTEE OF THE INTERNATIONAL ELECTROTECHNICAL COMMISSION

A considerable amount of technical work has been resumed actively during the year by the advisory committees of the International Electrotechnical Commission. The following meetings were held:

Group of experts on lamps, caps, and holders of advisory committee 6, London, May 1947

Advisory committee 30—extra-high voltages, Lucerne, October 1947

Advisory committee 12—radio communication, Lucerne, October 1947

Advisory committee 23—electric accessories, Lucerne, October 1947

International special committee on radio interference, Lucerne, October 1947

In addition, a meeting of the council was held in Zurich in June 1947, and of the committee of action in Brussels in October 1947.

The USNC was represented at all of the meetings. At the group of technical meetings held in Lucerne, it was represented by a strong delegation. At these meetings, all of which were the first technical ones since the war, substantial progress was made in getting under way the work of the advisory committees concerned. The European representatives are extremely anxious to meet with their colleagues from the rest of the world because during the war they had no opportunity to exchange information of a technical nature. They are anxious to press the work of the commission as much as possible, as they believe that this work will assist them in the many reconstruction and rehabilitation projects with which they are faced through war damage in their countries and through the demands of construction delayed during the war years.

The meetings of the council and the committee of action were concerned primarily with expediting resumption of technical work and with consummating the affiliation of the International Electrotechnical Commission with the International Organization for Standardization, as its electrical division. This affiliation now has been completed by its approval by the national committees. The central office of the IEC has been moved from London to Geneva where it occupies a building jointly with the ISO. It should be pointed out that the IEC retains its name, procedure, and freedom of action. It confidently is expected that this affiliation will give impetus and strength to the international standardization movement and that there will be important economics as well.

Meetings of the following advisory committees will be held in London and Paris in July, 1948

Advisory committee 30—Extra-high voltages
Advisory committee 31—Flameproof enclosures
Advisory committee 32—Fuses
Advisory committee 28—Co-ordination of insulation

The Swedish national committee has invited additional advisory committees to meet in Stockholm in October 1948. The committees which are to meet have not been definitely agreed upon.

During the year a reorganization of the electrical standards committee has been under way and has resulted in the addition of a number of new organizations in the field of electronics and communications. The membership of the ESC forms the nucleus of the USNC. While this matter was pending, no meeting of the USNC was held. Now that the membership has been completed, a meeting soon will be held. The work of the USNC has been carried out during the year by correspondence and through meetings of various technical advisory groups.

Technical Committees

Power Group

COMMITTEE ON ELECTRONIC POWER CONVERTERS

The status of this committee was changed, at the beginning of the 1947–48 administrative year, from a subcommittee of the electronics committee to a full committee in the power group. The membership was enlarged, and several liaison members from other committees were added.

Six subcommittees were established, as listed in the January 1948 issue of *ELECTRICAL ENGINEERING*. The organization of a West Coast subcommittee was initiated, and a chairman was appointed.

At the first meeting of the committee held in Pittsburgh on October 6, 1947, the subcommittees were organized, and the activities for the year were planned. A second meeting was held in Syracuse, N. Y., on May 17. Most of the subcommittees held meetings during the year.

A conference on the operation of mercury-arc rectifiers was held in Montreal on June 13, 1947, during the AIEE summer general meeting, under the sponsorship of the committee. Digests of the papers were published in the October 1947 issue of *ELECTRICAL ENGINEERING*.

A conference on mechanical rectifiers was held in Pittsburgh on January 29, 1948, during the AIEE winter general meeting, under the joint sponsorship of this committee and the electronics committee.

A working group of the committee has prepared a paper "Mercury-Arc Power Converters in North America," for presentation at the AIEE summer general meeting in Mexico City. The paper is a comprehensive report on the design and application of mercury-arc power converters.

The committee is planning to sponsor a conference on education in power electronics, in co-operation with other AIEE committees, to be held during the 1949 winter general meeting.

In addition to the committee activities outlined, several of the subcommittees carried on activities within their scopes.

Electronic Converter Application Subcommittee. This subcommittee is preparing questionnaire forms for collecting data on the operation and maintenance of mercury-arc rectifiers and on the characteristics of the a-c and d-c power circuits in rectifier installations. A working group was organized and started the preparation of a report on inductive co-ordination relating to the d-c circuits of rectifier installations. This report will be similar to the committee report "Inductive Co-ordination Aspects of Rectifier Installations," published in 1946, which dealt with the a-c systems of rectifier installations.

Electronic Converter Circuits Subcommittee. This subcommittee is preparing a report on protection of electronic power converters. The report is nearing completion, and it is planned to present it at the next Midwest general meeting of the AIEE.

Papers and Speakers Subcommittee. This subcommittee is preparing a bibliography on electronic power converters. The English-language section of the bibliography will be ready for publication this year.

Rectifying Devices Subcommittee. The chairman of this subcommittee is participating in the preparation of standards for mercury-arc pool-cathode tubes, which has been started under the sponsorship of the electron tubes subcommittee of the electronics committee. The subcommittee has not undertaken any independent activity.

Rectifier Transformers Subcommittee. This subcommittee is planning a comprehensive paper on the design and characteristics of rectifier transformers.

A proposal has been submitted by the chairman of the electronic power converter committee for enlarging the scope of the committee to include hot-cathode electronic converters, metallic rectifiers, and mechanical rectifiers, in addition to mercury-arc power converters. If this proposal is approved, the hot-cathode electronic converters subcommittee of the electronics committee and the metallic rectifiers subcommittee of the chemical, electrochemical, and electrothermal committee will be combined with the electronic power converter committee. It has been proposed also to change the name of the combined committee to "rectifier committee."

COMMITTEE ON CARRIER CURRENT

In this first year of operation, three full committee meetings have been held and in addition one meeting of subcommittee chairmen. Considerable time has been spent reviewing the activity of the previous joint subcommittee. It was decided to set up project subcommittees for the most important needs only and to retain other activities on file until additional personnel became available. Project subcommittees and status of work are as outlined in the following:

Preparation of an application guide. Work in progress.

Establish methods of measurements at carrier frequencies. Preliminary report circulated for comments. Expected to be ready for presentation as a technical paper in fall of 1948.

Obtain data on power system characteristics at carrier frequencies. Questionnaire sent out to obtain available test data.

Power equipment characteristics at carrier frequencies. Plans made for laboratory tests.

Report on features of traps and coupling capacitors for rural line carrier. Draft of report circulated for approval.

Report on microwave applications. Draft of report circulated for comments. Investigation of reliability of carrier relay channels questionnaire being prepared.

The committee did not sponsor any papers during the year. Plans are being developed for future technical papers and reports.

COMMITTEE ON POWER GENERATION

Concrete evidence of the committee's activities lies in the 13 papers presented at two meetings during the year. Practicality of the programs was attested by the large attendance that they attracted, and the extensive discussions that developed. The session sponsored by the committee at the Midwest general meeting in Chicago was devoted primarily to four papers on the determination of system generating capacity using the probability theory as a basis. The committee report on "Progress in Power Generation, 1940–46," also was presented at this time.

At the winter general meeting in Pittsburgh the power generation session was devoted to four papers on generator excitation systems and to four papers on hydraulic-turbine governor specifications.

The committee held meetings in conjunction with these two general meetings at which it organized its work for future technical sessions. Subcommittee activities reported at these meetings are briefed in the following:

Prime Movers. A symposium on gas turbines is in the process of organization planned for presentation at the next winter general meeting. Steps are being taken to co-ordinate work on a program for promulgating

recommended standardization of industrial steam turbines.

Speed Governing. This subcommittee working jointly with the American Society of Mechanical Engineers has issued two specifications to be used on a one year trial basis for steam turbines driving electric generators from 500- to 7,500-kw capacity, and for hydraulic turbines driving electric generators. Continuing efforts are being made in the solicitation of papers on prime-mover speed governing for general educational needs.

Excitation Systems. Papers are in preparation on excitation systems for presentation at coming meetings. The subcommittee now is working on an outline for "Definitions and Terminology on Excitation Systems for Synchronous Machines."

Station Design. Work is under way in organizing sessions on station descriptions and on automatic and supervisory control systems for central stations.

Hydroelectric Systems. Four papers have been arranged for presentation at the Pacific general meeting on the subjects of station bus arrangements, transmission line and switchgear connections, and pumping at various plants.

Other papers are in preparation for future meetings on the subjects of Grand Coulee and adaptation of generators to synchronous condenser service.

Pacific Coast. This subcommittee was organized to give a broader opportunity for members on the West Coast to participate directly in the organization of committee technical sessions. From several meetings of these members an extensive list of proposed subjects for future meetings was developed.

COMMITTEE ON RELAYS

In its first year of full committee status, the AIEE relay committee sponsored six formal papers, eight conference papers, and three conference sessions. The committee has contributed suggestions to assist with the expanded number and scope of technical committees and has engaged in joint projects with other committees. The principal technical activities were carried on through project committees, which are appointed to fulfill a specific assignment and discharged when the project is finished. In addition to three meetings of the full committee, practically all of the project committees have held one or more group meetings.

Generator Protection. To supplement previous information developed on the relay protection of generators, the project committee is making specific investigations of field protection, ground protection with high impedance neutral grounding, protection against motoring, tripping schemes for differential relays, and back-up protection. When these investigations are completed, the project committee findings will be summarized in a committee report.

Bibliography of Relay Literature. A formal paper, "Bibliography of Relay Literature—1944–1946," was presented (by title) at the January 1948 winter general meeting, and is being published in the *PROCEEDINGS*. This bibliography contains a list intended to include all technical articles published in the American and foreign press dealing with protective relaying. Collection of information is continuing so that another publication will be available, probably in 1950.

Co-ordination of Construction and Protection of Distribution Circuits (Joint Group). A study of the correlation of distribution circuit protection practices and construction methods, and the influence of these on the reduction of sustained faults, first was undertaken as a joint project of the (former) AIEE relay subcommittee and distribution subcommittee. The need for developing adequate performance records led to the expansion of the working group into one sponsored jointly by the relay committee and the distribution subcommittee of the AIEE, and the transmission and distribution committee of the Edison Electric Institute.

At a conference session held on January 27, 1948, in Pittsburgh during the winter general meeting, the performance of several distribution systems was summarized, with emphasis on schemes for instantaneous initial circuit breaker opening to protect sectionalizing fuses for transient faults, with subsequent time delay tripping to blow the fuses for sustained faults. Load inrush problems and ground relay protection also were discussed.

A survey is being prepared to obtain data on the relative operating characteristics and resulting degree of service continuity obtainable with various protection methods and construction practices; also, the presentation of formal papers is being encouraged.

Standards for Power Relays. The project committee on Standards for power relays has reviewed the comments and suggestions received following the circulation of a preliminary draft of a proposed revision of ASA Standard 6-37.1. A second draft of the proposed revision is nearly completed and will be circulated to the committee membership soon.

Insulation Levels for Relays and Control Circuits. The results of a survey conducted by the project committee appointed to study desirable insulation levels for relays and control circuits have shown that a test voltage of 1,500 volts alternating current should be adequate for the current, potential, d-c control, and tripping circuits of protective relays. Also, the need has been emphasized for close attention to the overvoltages introduced into control circuits by inductive "kicks" of closing solenoids,

The influence of relay contact spacing on breakdown voltage and on relay operating speeds is being investigated. A formal paper is being prepared outlining the basic factors of this problem and describing methods for suppressing transient overvoltages in relay and control circuits.

Sensitive Ground Protection. The project committee has collected information on available methods for sensitive ground relay protection of transmission and distribution circuits. Several methods used for transmission circuits and unigrounded distribution systems have been reviewed and the investigation of possible methods for improved sensitivity with 3-phase 4-wire common neutral circuits is being continued. A committee report should be available during the next administrative year, summarizing successful methods of sensitive ground protection of three wire systems and outlining the difficulties and limitations of schemes for four wire multigrounded neutral systems.

Transmission Line Protection. Analyses of a hypothetical, heavily loaded transmission system composed of short, medium length, and relatively long transmission lines have been made to crystallize the basic requirements of relay protective schemes and limitations of available methods.

In view of the increasing awareness of the need for improved transmission line performance required by the trend toward higher economic loading of lines, a conference session was held at the January 1948 winter general meeting. Three conference papers were presented at this meeting dealing with the relay protection of short, medium, and long transmission lines. This investigation is being continued along with the encouragement of formal papers.

Electronic Relay Applications. A survey of current practices has been made to determine the existing applications of electronic devices to protective relaying systems. The proper place of electronic devices in the protective relay field has been the subject of widely divergent views, some holding that they should be used whenever possible and others maintaining that applications should be limited to those functions which cannot be performed adequately by electromechanical elements.

To develop reasons for these divergent opinions a conference session was sponsored at the Chicago meeting of November 1947 with one introductory paper presenting the more favorable aspects of electronic relay applications and another covering general considerations including the limitations of such elements. It is believed that this subject warrants the presentation of formal papers and these are being encouraged.

Relay Performance of Current Transformers (Instrument Transformer Subcommittee). A new project committee was organized in September of 1947 to study the relaying performance of current transformers, with emphasis on the extension of methods for calculating the performance of those having high impedance secondaries. Attention also was directed to the wide variations in characteristics of auxiliary current transformers and certain main current transformers designed for mounting in the small space of metalclad switchgear.

As a result of a decision by the power co-ordinating committee, instrument transformers were placed under the scope of the transformer committee, and that committee organized an instrument transformer subcommittee with representation from the relay and other committees. Since December 1947, this project is being continued under the instrument transformer subcommittee.

Grounding of Instrument Transformer Secondary Circuits. Following the publication of a committee report on "Grounding of Instrument Transformer Secondary Circuits," in the April 1947 *ELECTRICAL ENGINEERING*, it has been agreed that the material should be converted into a form suitable for an operating guide and incorporated in the AIEE Standards. Since such a guide would be included most logically in the Standards on instrument transformers, the material and subsequent comments have been turned over to the instrument transformer subcommittee (of the transformer committee) for inclusion in the next revision of these Standards.

Relaying of Interconnections Between Industrial and Utility Generating Systems. A new project committee was organized in September of 1947 to investigate the special relaying problems encountered in the parallel operation of industrial generating systems with those of utility generating and transmission systems. The work is beginning with a survey of current practices.

Protection of Stations Without High-Voltage Switching. In September of 1947 a new project committee was organized to study various methods which might be used for the relay protection of stations and equipment which are not provided with high-voltage circuit breakers. The improved reliability of power transformers has led to the increasing use of

such schemes, and a survey to determine present practices is being undertaken.

COMMITTEE ON ROTATING MACHINERY

At the beginning of the 1947–48 Institute year the former committee on electric machinery was, under the new co-ordinating group organization, divided into several committees, one of which is the committee on rotating machinery. The first problem facing this newly designated committee was to organize so as properly to assume its assigned responsibilities. These included the previous functions of the electric machinery committee as related to rotating machines plus an expanded program to include classes of such machinery which it was felt previously had not been covered adequately. This organization took the form of a main committee with seven subcommittees—administrative, synchronous machinery, induction machinery, d-c machinery, single-phase and fractional-horsepower machinery, insulation, and test code.

The first meeting of the reorganized committee was held in Pittsburgh during the winter general meeting, January 1948. At that meeting, Mr. Steinberg, chairman of the power co-ordinating committee, gave a brief history of the new co-ordinating group activities and stated that after six months of operation, the organization is working well. He then asked for comments from the members of the rotating machinery committee as to their opinions both on the power group functioning and on their own committee activities. The general concensus of opinion indicated a very favorable acceptance of the entire new organization.

The machinery subcommittees then submitted reports on their activities:

Synchronous Machinery. A meeting was held at the winter general meeting at which recommendations were submitted for revisions of test code number 503, and suggestions made for better co-ordination among other codes and standards.

Induction Machinery. Similar actions with respect to codes and recommendations as for synchronous machinery.

D-C Machinery. A meeting was held during the Chicago meeting and another during the midwinter meeting. The program for more accurate loss evaluation and temperature determination by test is proceeding, and recommendations incorporating the results of the program in various test codes and standards will be made. A carbon brush test code is in process under joint sponsorship of this and another committee.

Single-Phase and Fractional-Horsepower Machinery. This is a newly organized subcommittee. The committee plans to study the proper scope of its activities as co-ordinated with other subcommittees, the preparation of test of codes, and the submission of recommendations for standards.

Insulation. A meeting was held at the winter meeting at which time a preliminary draft of the proposed Standards on insulation resistance of d-c machinery was discussed. It was decided to rewrite completely AIEE Standard 43 to include d-c machinery.

Following the submission of these reports, the main committee discussed them and other business. The scope of the insulation subcommittee was broadened from its previous activity of insulation resistance to cover all proper types of activity on rotating machinery insulation. The main committee was informed of and approved an ambitious program to prepare an extensive bibliography on rotating machinery which would be submitted to the Institute as a basis for reference and for analysis of the published literature. The preparation of the application guides by the rotating machinery committee was deemed undesirable, however co-operation with industry committees will be undertaken gladly should they desire such guides. Business pertaining to joint action between the rotating machinery and other committees was discussed with recommendations for action.

During the period up to April 30, 1948, the committee as a whole, through its subcommittees, has sponsored six technical sessions including a major motor program at the Middle Eastern District meeting at Dayton. In all, 33 technical papers have been sponsored at four major meetings. AIEE Standard 43 has been written. Report *43-A*, "Recommended Practice for Insulation Resistance Testing of Rotating Machinery," soon will be ready for submission. Plans have been made for sponsoring a major rotating machinery meeting during the coming year.

COMMITTEE ON SYSTEM ENGINEERING

This committee is a new technical committee established by the reorganization of the technical committees of the power group for this year. In view of this, a completely new committee organization had to be set up. There was no committee work or investigations carried over from the previous year as is the usual case with standing committees. In the formation of the committee, a definite attempt was made to secure as country-wide representation as possible and this also was carried out in the establishment of the subcommittees. There were six subcommittees established as follows: administrative, system planning, system operations, system economics, system controls, and interconnection contracts.

The scope for the system engineering committee was reported in the October 1947 issue of *ELECTRICAL ENGINEERING*. The membership and scopes of the subcommittees were given in detail in the January 1948 issue of *ELECTRICAL ENGINEERING*.

The first meeting of the system engineering committee was held on November 5, 1947, in Chicago, Ill., during the Midwest general meeting with 18 members or their representatives in attendance. The scopes and memberships of all the subcommittees were given a final review and assignments were made to the individual subcommittees as to investigations and solicitation of papers for future AIEE meetings.

A second meeting of the system engineering committee was held on January 29, 1948, in Pittsburgh, during the winter general meeting with 21 members and guests present. At this time the individual subcommittees reported as to their work and their plans in progress. Plans were made for solicitation of papers for presentation at the Midwest general meeting in the Fall of 1948. It was announced that a joint subcommittee had been appointed on the "application of probability methods to capacity problems." This is to be a joint subcommittee with the power generation committee.

A general system engineering session was sponsored at the winter general meeting in Pittsburgh, at which five conference papers were presented. The first of these was on the timely subject "Electric Power and Our National Defense." The other four conference papers were a symposium on the "Controllable Factors Affecting System Loads." These were believed to be of importance in view of the tight capacity conditions existing throughout the United States at present.

A special subcommittee has been established under the sponsorship of the switchgear committee to investigate "high-voltage switching where out of phase voltages exist at time of arc interruption." The system engineering committee, together with the power generation, relay, and transmission and distribution committees, is providing for representation on this working subcommittee.

At the time of rendering this report, the several subcommittees have in preparation technical papers for at least two and probably three full sessions at the Midwest general meeting in Milwaukee, October 18–22, 1948.

The system controls subcommittee is preparing a questionnaire to be sent to operating companies to summarize the status of load-frequency control equipment and its use on the various individual and interconnected systems. It is expected that this report will be available for the Milwaukee meeting.

COMMITTEE ON TRANSMISSION AND DISTRIBUTION

Since the reorganization of the technical committees and reallocation of scopes, three meetings of the transmission and distribution committee have been held, the first on December 5 in Chicago, the second on February 25 in Cleveland, the third on May 28 in New York. The meetings were well attended. Following the formal meeting in Cleveland a general discussion organized previously by the lightning and insulator subcommittee was held on the subject of insulator contamination. Two prepared discussions were presented after which a general discussion followed.

Technical Meetings. Two technical sessions were sponsored by the transmission and distribution committee at the Midwest general meeting at which papers dealing with the Tidd 500-kv project were presented. A joint session at this meeting also was sponsored by the transmission and distribution committee. At the winter general meeting a session dealing with the Duquesne Light 10,000-kva series capacitor was sponsored. Two technical sessions sponsored by the transmission and distribution committee are being held at the Mexico City meeting.

Capacitor Subcommittee. The capacitor subcommittee is engaged in the revision of AIEE Standard 18 on capacitors. A final draft of the proposed Standard will be completed this year. This subcommittee also is concerned with operating procedures and application requirements for capacitors and is investigating troubles that have occurred with capacitors.

Distribution Subcommittee. The principal activity of the distribution subcommittee was concerned with the preparation of a report entitled "Distribution Design Practices and Trends," to be presented at the summer general meeting. This subcommittee also has in mind the organization of a symposium during the 1949 winter general meeting on the "Operation of Primary Network Systems." Consideration also is being given to the question of sag data for distribution conductors.

General Systems Subcommittee. The general systems subcommittee has completed a report on "Power System Recovery Voltages." A working group of this subcommittee actively

is engaged jointly with the lightning protective devices subcommittee of the protective devices committee in the determination of "the voltage recovery of typical distribution circuits."

Lightning and Insulator Subcommittee. This subcommittee has under preparation a report entitled "Recommendations for Lightning-Proof Lines." They also actively are sponsoring papers concerned with insulator contamination and corrective measures. A symposium on this subject is being organized for the Midwest general meeting. This subcommittee also is concerning itself with the development of additional data on 60-cycle and impulse flashover characteristics of disk-type insulator strings made up of between 20 and 30 units. This is for use in connection with transmission voltages in the range of ultrahigh voltages.

Towers, Poles, and Conductors Subcommittee. A report has just been completed by this subcommittee entitled "Conductors—Types, Availability, and Use," which will be presented at the summer general meeting. Other subjects being considered by this committee are the thermal limitations of conductors and clamps and radio interference emanating from corona.

Co-ordination with Edison Electric Institute. An effort is being made to engender closer co-operation between the transmission and distribution committee of the EEI and transmission and distribution committee of the AIEE. To this end a proposal is being considered to have mutual representation of responsible officers on the respective committees. A striking example of this co-operation is the organization of the subject committee on line outages of the transmission and distribution committee of the AIEE, in which the EEI committee will be represented officially by a member. The function of this committee is to collect information on various phases of line outages. The EEI representative will report the progress of the EEI committee and operate in a general liaison capacity. Similarly, with regard to the study being made by the capacitor subcommittee with relation to capacitor troubles, Howard Stites is the EEI representative on this committee work.

Industry Group

COMMITTEE ON CHEMICAL, ELECTROCHEMICAL, AND ELECTROTHERMAL APPLICATIONS

The committee held one general meeting during the year, in Pittsburgh, January 1948. The accomplishments of the past calendar year were reviewed and plans discussed for the coming year.

A new subcommittee on chemical industries was formed and some personnel appointed. The scope of this subcommittee is to cover the interests of the chemical industry exclusive of electrolytic or electrothermal processes.

It is believed that this technical committee now is organized logically into its functional subcommittees, and personnel and chairmen have been secured. The subcommittees now are functioning.

This technical committee consists of the following subcommittees:

1. Electrolytic processes
2. Batteries, battery forming, and charging
3. Arc furnaces and electrothermal processes
4. Metallic rectifiers
5. Petroleum refining and production
6. Cathodic protection
7. Chemical industries

The special subcommittee on voltage transients in arc furnaces has died without issue.

The following activities were engaged in during the year by the subcommittees:

1. *Electrolytic processes*

Held a conference on conversion equipment in the electrochemical industry in Montreal, June 1947. Conference papers published in booklet form together with formal papers sponsored by power electronics group.

Held a meeting of this subcommittee in Montreal and discussed future plans.

2. *Batteries, battery forming, and charging*

Held a conference on the care, maintenance, and charging of storage batteries in Chicago, October 1947. The meeting of the subcommittee discussed possible future activities and work that needs to be done.

3. *Arc furnaces and electrothermal processes*

Held a conference on electrothermal processes in Pittsburgh, January 1948. Several of these conference-type papers are being prepared for publication.

4. *Metallic rectifiers*

A subcommittee meeting was held on October 11, 1947, in New York to discuss plans for the 1948 winter general meeting.

Held a conference in Pittsburgh, January 1948, with one formal paper and five conference-type papers. The subcommittee also held a committee meeting to discuss Standards for metallic rectifiers and to plan future activities. The proposed Standard for metallic rectifiers was published for comments and criticism on August 1, 1946; revised December 2, 1946, April 18, 1947, June 9, 1947, and July 1947.

5. *Petroleum refining and production*

While no formal meeting of the subcommittee yet has been held, two formal papers are being sponsored for the Mexico City meeting.

COMMITTEE ON ELECTRIC HEATING

The board of directors authorized in January 1947 the formation of a technical committee on electric heating. During the past year this committee has been organized and its work initiated with the assistance of several subcommittees. These subcommittees include the following: electronic heating, radiation measurements above 200 megacycles, resistance heating and electric furnaces, and radiant heating. The first meeting of the electric heating committee was held on January 27, 1948, in connection with the 1948 winter general meeting. The committee sponsored a session at this meeting on electronic and resistance heating.

The work of the subcommittee on electronic heating on interfering radiations from induction and dielectric heating equipment has been continued. Upon the advice of this subcommittee the Federal Communications Commission has promulgated rules and regulations regarding interference from such equipment. A working group of the subcommittee now is engaged in studying the matter of good engineering practice in the case of interference. Another working group has initiated the preparation of Standards on induction and dielectric heating. Because of the interest of Institute of Radio Engineers and National Electrical Manufacturers Association in these matters it was thought advisable to form a joint co-ordination committee to distribute the work among the committees of the various interested societies. This committee is known as the joint AIEE, IRE, NEMA co-ordination committee on commercial induction and dielectric heating apparatus. The AIEE representative on this committee is the chairman of the electric heating committee.

The subcommittee on radiation measurements above 200 megacycles has studied equipment and methods for measurement of radiation in the frequency range from 200 megacycles to 20,000 megacycles. This work, of course, is in preparation for the extension of dielectric heating into this frequency range. The techniques and equipment used for such measurements differ widely from those employed at lower frequencies, so that much original work has been required which eventually will be presented to the members of AIEE in papers.

The subcommittee on resistance heating and electric furnaces held its first meeting in connection with the 1948 winter general meeting of AIEE. It plans active participation in a program to stimulate and standardize this form of heating in industry.

The subcommittee on radiant heating held its first meeting in February 1948. It plans an unusually large program covering the following: historical, methods of temperature measurement, Standards, coatings for infrared baking, patents, energy distribution, control equipment and safety, research, and Student Branches.

COMMITTEE ON INDUSTRIAL CONTROL

The scope of this committee has been broadened to include design, construction, and performance of industrial control equipment. Some progress has been made in increasing the number of industrial engineers in the membership of the committee.

The subcommittee on Standards actively is engaged in the revision of the present AIEE industrial control Standard in co-operation with the corresponding committees of American Standards Association and NEMA. A revised draft of the Standard soon will be ready for the Standards committee.

The subcommittee on electronic control has had an active year organizing its various working groups with a view to preparing circuit and component definitions associated with industrial electronic control. This subcommittee is represented on the committee on electronics and its work is being correlated closely with the more general committee. The working groups have defined their objectives which, when completed, should extend the usefulness of the present AIEE Standard on Industrial Control.

The subcommittee on test codes is proceeding with one or two projects which are part of an over-all program established during the year.

The bibliography continues to grow, and it is hoped that before long a completely indexed bibliography of extensive proportions will be available for publication.

The joint subcommittee on servomechanisms continues to act under the joint sponsorship of the committees on basic sciences and instruments and measurements. An interesting session was held at the winter general meeting. The activities of the subcommittee on electronic control is being correlated with this joint subcommittee to prevent overlapping. It is hoped that the joint subcommittee will sponsor more papers illustrating the application of servomechanism theory to industrial problems.

During the year the committee has been very actively engaged in soliciting papers. One session was sponsored at the 1947 sum-

mer general meeting, one at the Midwest general meeting, and two on steel mill applications at the winter general meeting, and another on wire drawing machinery at New Haven in April 1948. The sessions were well attended and brought considerable discussion from the membership.

Communication and Science Group

COMMITTEE ON COMMUNICATION

The communication phase of electrical engineering as practiced today covers a very broad and continually expanding activity. The committee on communication is constantly on the lookout for papers of interest to the specialist in communications, as well as papers of general interest to the electrical engineer. Special attention has been given to papers on radiobroadcasting and home receivers. Several sessions are being scheduled at the 1948 Midwest general meeting covering these subjects.

Full sessions were sponsored by the committee at all of the general meetings. Two sessions composed of eight papers were sponsored at the summer general meeting in Montreal. One session included papers on modulation, and the other the general subject of communication. At the 1947 Pacific general meeting held in San Diego, two sessions composed of 11 papers were sponsored by the committee. The papers covered the fields of communication, electronics, and Sonar. Two sessions composed of eight papers covering general and special communication subjects were presented at the Chicago Midwest general meeting. An extra effort was made at the Pittsburgh winter general meeting to highlight communication subjects. As a result, five sessions composed of 17 papers covering communication components and techniques, magnetic recording, synthetic crystals, facsimile, and radio relay systems were presented.

As the current fiscal year draws to a close, plans have been finalized for a 4-paper session at the Mexico City summer meeting and a 4-paper session for the Spokane general meeting. A 20-paper 5-session program is in the preliminary planning stage for the Milwaukee Midwest general meeting.

Papers solicited and reviewed by the committee are available for use at District meetings. Two requests for communication papers were received from Districts during the current fiscal year.

During the year 21 papers and reports sponsored by the committee were published in *ELECTRICAL ENGINEERING* or in *TRANSACTIONS*. Many more have been recommended for future publication.

Starting in August 1947, the committee on communication was reorganized. Nine subcommittees were appointed and cover the following fields:

1. Wire communication systems.
2. Radio communication systems.
3. Radiobroadcasting systems.
4. Communication switching systems.
5. Power line carrier systems.
6. Home radio receivers.
7. Record communication.
8. Special communication application.
9. Communication standards.

The subcommittee chairmen, committee chairman, vice-chairman, secretary, and junior past chairman, have composed a co-ordinating committee to direct, in general, the subcommittee activities and take final action on matters referred to the committee for attention. Two general meetings of the whole committee and three meetings of the co-ordinating committee have been held during the current fiscal year. The purpose of the foregoing reorganization is directed toward communication group status with full committee responsibility for the present subcommittees.

COMMITTEE ON COMPUTING DEVICES

The committee on computing devices was newly formed in January 1948. Most of the work has been as a subcommittee of the basic sciences committee. (There was a conference at Montreal, and a few papers published in *ELECTRICAL ENGINEERING*.)

The only committee activity has been organizational, that is, to appoint a vice-chairman (Doctor J. G. Brainerd) and a secretary (Professor W. C. Johnson), and to form two subcommittees, one on digital computers (Doctor J. W. Mauchly, chairman), and one on continuous-variable computers (Doctor E. L. Harder, chairman).

COMMITTEE ON BASIC SCIENCES

The committee on basic sciences occupies a unique position in the activities of the Institute. Its function is to advance the theory of electrical engineering and of the allied arts and sciences in all of their branches; consequently, efforts have been, and are being made, to assist the several other technical committees wherever such action is desired. Specific examples which may be mentioned are: the initiation of nucleonic studies by the subcommittee on energy sources, which subject subsequently was taken over by the full technical committee on nucleonics; formation of a joint subcommittee on servomechanisms, with the committees on instruments and measurements, and on industrial control devices; subcommittee on large scale computing devices, recently transformed into a full technical committee on computing devices; liaison members on subcommittees of the committees on electrochemistry and electrometallurgy, on systems engineering, and on power generation.

The subcommittee method of operation of the committee on basic sciences has proved to be highly successful during the past two years of its operation. It had been suggested by the preceding chairman of the committee during the war, but military restrictions at that time limited the activities of the committee, causing postponement of the program.

During the past year, the committee and its several subcommittees have held sessions, conferences, and symposia in their respective fields, as follows:

Summer General Meeting (Montreal), June 1947

1. Participation in conference on technical group activities in the Sections.
2. Session on servomechanisms.
3. Conference on large scale computing devices.

Midwest General Meeting (Chicago), November 1947

1. Conference on energy sources.
2. Session on basic sciences.

Winter General Meeting (Pittsburgh), January 1948

1. Session on basic sciences.
2. Conference on applied mathematics.
3. Symposium on electrical network theory.
4. Session on servomechanisms (combined with measurements).

The interest shown at these meetings has been quite gratifying, in most instances being among those sessions having the largest attendance at the technical meetings.

Four subcommittees have been in active work during the past year. These are: electric circuit theory, energy sources, applied mathematics, and large scale computing devices. As mentioned, the subcommittee on large scale computing devices has become a full technical committee, retaining the same personnel as that of the subcommittee. Two new subcommittees have been formed in the past few months, and a third is planned. Those now underway are the subcommittees on electrical properties of solids and on magnetics. The third is to be the subcommittee on electrical properties of liquids and gases. It is expected that these new subcommittees will be in a position to conduct conferences in their respective fields, beginning with the 1949 winter general meeting of the Institute.

As a number of the committee members also are members of subcommittee 1 of ASA sectional committee *C-42*, "Definitions of Electrical Terms," it is planned to establish definitions of fundamental and derived terms which are acceptable to the engineering profession as a whole. The committee on basic sciences will provide the desired reviewing body for this work.

As it is extremely difficult to hold committee meetings at frequent intervals, because of the wide dispersion of the membership, it has been the practice to hold committee meetings in connection with the general meetings of the Institute. The attendance at these meetings has been very good, which along with the correspondence among members, has enabled the committee to function quite efficiently.

COMMITTEE ON ELECTRONICS

The committee has continued the policy of subcommittee rotation, that is, of forming and disbanding, as needs require, subcommittees in various fields of electronic design and application. The purpose of this policy is to insure active AIEE contact and support in all areas of the expanding electronic arts; it calls for occasional upgrading of major subcommittees to the status of technical committees of the Institute.

There are two types of subcommittees within the committee organization. One type serves to expedite general functions, including: processing of Standards; papers solicitation and aids to Sections; maintenance of nation-wide and international informational contacts; education; and liaison with other professional groups. The other type is responsible for attention to specific technical fields. The committee entered the year with five general-function subcommittees, and added three, thus closing the year with eight such groups. At the beginning of the present administrative year there were six subcommittees having specific technical activities; and five new ones were formed; thus the electronics committee closes the year with a total of 19 subcommittees of which number 8 are administrative and 11 are technical.

Plans of operation for the 1947–48 period were formulated at a meeting of subcommittee chairmen in New York City on

August 26, 1947. A meeting of the committee was held in Chicago on November 4, 1947, in connection with the National Electronics Conference, where procedures involved in establishing Standards were discussed in some detail. A meeting held at Pittsburgh on January 26, 1948, in connection with the winter general meeting, was devoted to reviews of plans for technical sessions, conferences, and papers, and to reports on the accomplishments of the various subcommittees during the year past.

At the 1947 summer general meeting in Montreal the committee sponsored a technical session, and a conference on rectifiers. The committee organized a technical session of the National Electronics Conference at Chicago on November 4. At the winter general meeting in Pittsburgh the committee sponsored a technical session, also a technical conference on, "Some New Electronic Devices and Methods," also, the committee shared with the rectifier committee the sponsorship of a conference on mechanical rectifiers. For the summer general meeting in Mexico City the committee has arranged two technical papers, and has assisted the committee on education in arranging a discussion of certain educational aspects of electronics.

Through the medium of the joint subcommittee on electronic instruments, the committee had an important share in the planning of the 2-day AIEE conference on electron tubes, held in Philadelphia, March 29 and 30. The objective of this conference was to study the problem of making generally available electron tubes meeting specifications needed for electronic instruments and related industrial electronic devices. This conference consisted of five technical sessions, the three on the first day being devoted to study of the factual background of the problem, and those on the second day to methods of meeting the existing needs. The conference was very well attended; there was active discussion; and it is believed the program was an important contribution.

During the calendar year 1947, *ELECTRICAL ENGINEERING* contained a very large proportion of articles including important electronic material. The activities of the committee and its subcommittees have contributed substantially to this result.

Activities of the subcommittees of the general-function type are reported in the following.

Electronic Papers Solicitation and Section Contacts. This subcommittee continually has solicited technical papers from the other subcommittees. It has provided a convenient channel for originating and selecting suitable papers, and for organizing technical sessions relative to all fields of electronic application. This subcommittee sponsors annually at the winter general meeting a conference on new electronic devices and methods. Active encouragement has been given to other technical committees of the Institute to sponsor electronic papers at appropriate technical sessions.

Sections of the Institute have been aided materially by this subcommittee's activity in providing films, speakers, and technical information on electronic subjects.

Standards. This subcommittee keeps the committee members informed as to standardizing procedure, also as to the progress of specific standard proposals, and serves as a channel for processing Standards. It is at present active in attempting to establish a uniformity between American Standard symbols and those recommended by the Patent Office, in regard to electronic diagrams. This subcommittee is maintaining contact with certain IRE committee activities of importance in regard to Standards in radar and frequency designation.

Liaison With IRE. It is essential to maintain proper contact and co-ordination of committee activity between various professional groups active in electronic matters. It is this subcommittee's responsibility to maintain an awareness of co-ordination needs, particularly relative to the IRE.

Liaison With Joint Electron Tube Engineering Council. It is important that the committee be kept informed of the needs and interests of the JETEC in matters of policy, co-ordination as to area of standardization, and so forth. This liaison has been provided by the subcommittee on liaison with JETEC.

Liaison With Nucleonics. Most of the instruments and apparatus employed in neucleonics are electronic in character. This liaison committee was formed to keep the electronics committee informed of the work of the neucleonics committee.

Electronic Developments Abroad. This subcommittee has prepared a survey report of recent progress in power electronics in several European countries, including extracts from extensive foreign reports received in reply to the subcommittee's inquiries. The information relative to new uses of d-c transmission is of special interest. This report should appear in an early issue of *ELECTRICAL ENGINEERING.*

Education in Electronics. This newly-formed subcommittee will acquaint the committee with progress and problems related to rapidly changing requirements for education in the electronic areas of electrical engineering, and co-ordinate the committee's activities with those of the AIEE committee on education.

West Coast Subcommittee. This newly-formed subcommittee will be concerned with securing papers of electronic interest from the West Coast area, particularly for meetings in that area, and with stimulating active participation from that area in AIEE technical committee work, where electronic problems are involved.

The activities of subcommittees working in specific technical fields may be summarized as in the following:

Electronic Instruments. The joint committee on electronic instruments completed its survey of special tube needs for instrumentation purposes, and organized the afore-mentioned 2-day tube conference. Participation in this survey and conference activity has been one of the most important contributions of the committee on electronics during the past year. This activity is being continued. Five technical papers and three conference papers were obtained for the Pittsburgh meeting by this subcommittee; the technical papers are being processed for publication. A program of Standards work in the electronic instrument field is being formulated.

Electron Tubes. The scope of this subcommittee is the treatment of all appropriate technical matters relating to the design, construction, test, and operation of electron tubes for industrial use, including the preparation of Standards, papers, and reports, and the promotion of meetings and conferences dealing with electron tubes and their uses. All classes of electron tubes important for industrial use are included in the intent of this scope.

Four working groups have been organized to give immediate attention to the preparation of Standards for tubes used industrially. The working groups deal respectively with high vacuum tubes, cathode-ray tubes, gas-filled tubes and photoelectric tubes, and mercury-pool tubes. The initial meetings of the various working groups were held during the early months of 1948.

Electronic Aids to Medicine. This subcommittee is planning a 2-day conference, to be held in New York City in the Fall of 1948. Arrangements are being set up to attract both engineering and medical personnel, in order to promote a better understanding by engineers of the engineering problems peculiar to biological practice, and by medical personnel of possible engineering approaches to biological practice. Subjects discussed at the conference will include biological amplifier and recording device requirements, practice in biological stable isotope measurements, and present practice in biological radioactivity measurement.

Electronic Aids to Navigation. This subcommittee operates jointly with an IRE committee on the same subject. This group has compiled a set of more than 200 basic definitions of radio navigation terms which currently are being compared, for purposes of mutual revision, with a set of similar definitions by the Radio Technical Commission for Aeronautics.

High-Frequency Conductors, Cables, and Connectors. The scope of this subcommittee is to give attention to Standards, papers procurement, and so forth, to the developing needs for adequate handling of power and signals in high-frequency coaxial and wave guide circuit elements, including problems of heating, reflection, and impedance characteristics.

Hot Cathode Converters. This subcommittee has completed its immediate Standards program on definitions, except for the adjustment of a few inconsistencies between American Standard Definitions and proposed Standards for mercury arc power rectifiers and for metallic rectifiers. The committee now is compiling a list of circuits to be included in the Standards. The committee is endeavoring to procure technical papers which will clarify existing uncertainties as to test code basis in hot cathode rectifier equipment in which transformer reactance is important.

X-Ray Tubes and Apparatus. This committee recently has been reorganized, to broaden its scope. This group currently is reviewing recently proposed Standards, and is studying means of specifying X-ray and associated rectifier tube characteristics and ratings. Arrangements have been made for co-ordination of the Standards activity with the work of NEMA and Radio Manufacturers Association groups having similar interests.

Electronic Precipitation. This subcommittee is planning papers for a technical session on electronic precipitation equipment, with a later one in prospect relative to electrostatic phenomena in industry.

Electronic Aids to Geophysics. The immediate objective of this recently formed subcommittee will be to give AIEE support to efforts being made, by co-ordinated technical societies in the oil-producing geographical area, toward broader publicizing in technical periodicals of the many recent important developments in this field of interest.

Infrared Applications. This subcommittee has been very recently formed, and is at present making a study to outline its scope and immediate objectives.

Electronic Preparation and Preservation of Food. This subcommittee's present status is the same as that on infrared applications.

The committee, as a whole, is attempting to maintain a vigorous outlook to insure adequate coverage of the various fields in which electronics is becoming of importance in science, industry, and government.

COMMITTEE ON INSTRUMENTS AND MEASUREMENTS

The activities of this committee have been directed toward the fostering of interest, the co-ordination of effort, and the development of Standards in the field of measurements which underlies so much of the technical activity of the Institute. To secure adequate representation in some of the newer phases of measuring activity, and provide closer liaison with other technical committees, the membership of the committee was expanded to 41. Meetings of the main committee were held on October 21, 1947, and March 16, 1948, and there have been numerous meetings of subcommittees.

A total of 25 technical papers sponsored by the committee were presented during the year, and most of these are being published. These papers cover a wide range of subject matter, including: fundamental analyses of measuring methods; microwave frequency measuring devices, methods, and Standards; new types of telemetering systems; oscillograph and oscilloscope techniques; industrial electronic instruments for steel processing; laboratory measuring standards; d-c amplifiers and other electronic circuits; nucleonic instrumentation; calorimetric measurements; characteristics of recording instruments; optical radar; supersonic devices; instrument characteristics; recording of sea water characteristics.

The committee sponsored two technical sessions at the winter meeting and was joint sponsor for three others. Additional papers were presented at the Great Lakes and North Eastern District meetings.

A large part of the work is carried out through nine subcommittees.

Subcommittee on Revision of Standard 4 (Measurement of Test Voltage in Dielectric Tests). A working group is drawing up a specification for measurements of surge voltages at intervals from 0.1 to 1 microsecond. Additional material is being prepared on other measuring methods.

Subcommittee on Revision of C39 (Electric Indicating Instruments). This subcommittee is co-operating with the ASA subcommittee on a complete revision of the *C39* standard. The work on panel-type and switchboard instruments is substantially complete and will be presented for approval at an early date. Consideration of portable instruments and laboratory Standards will follow.

Subcommittee on Master Test Code for Resistance Measurements. A proposed new code was published in September 1947 for one year's trial use. Comments are being collected.

Subcommittee on Master Test Code for Temperature Measurements. At the suggestion of the Standards committee, a proposed new code has been circulated for comment. It now is being presubmitted with the recommendation that it be printed for trial use.

Subcommittee on Marking of Varmeters and Related Instruments. A questionnaire is being circulated to determine types of instruments and their markings.

Subcommittee on Watt-hour Meters. Activities were limited to minor standardization matters.

Subcommittee on Electrical Tests on Dielectrics in the Field. A general report and bibliography on this subject are being prepared. A technical conference is planned for the Milwaukee meeting in October 1948.

Subcommittee on High-Frequency Measurements. The field of this subcommittee includes all frequencies above the audio range, with special emphasis on the higher radio frequencies where the more recent developments in the art are found. A technical session was sponsored at the winter meeting. A symposium is planned under the joint sponsorship of AIEE, IRE, and Bureau of Standards.

Subcommittee on Definitions. A large number of definitions prepared by this subcommittee have been transmitted to appropriate subcommittees of ASA *C42*. Definitions prepared by Subcommittee 6 of ASA *C42* are being reviewed by this subcommittee.

Close relationship and co-operation with other technical committees is fundamental to the work of the committee on instruments and measurements. Co-ordination is maintained by a number of joint subcommittees, and in other cases by cross representation. During the year, one new joint subcommittee was formed to centralize all Institute work on instrument transformers. This is under the direction of the instruments and measurements committee and the transformer committee, with representation also from the switchgear committee, the relay committee, the transmission and distribution committee, and the carrier current committee. For better co-ordination, the chairmen of all the standing joint subcommittees have been made members of the instruments and measurements committee.

Joint Subcommittee on Electronic Instruments. This subcommittee completed its survey of the needs and requirements for improved electron tubes for industrial instrumentation and control. A 2-day conference was held at Philadelphia to present the results of the survey and outline a program for obtaining improved tubes. A registration of 281 attested to the success of this conference. Contributing largely to this success was the community of interest in the subject. The conference has given a needed impetus to improved tubes for industrial usage, and should have important results in future. Moreover, it has established a pattern which other committees are planning to follow.

Joint Subcommittee on Servomechanisms. This subcommittee has been reorganized. Two technical papers were presented at the winter meeting.

Joint Subcommittee on Nucleonic Instruments. A conference session on instruments for radiation measurement was sponsored at the winter meeting, and similar sessions are planned in August and October. A paper on "Methods of Protection and Measurement of Nuclear Radiation," was published.

Joint Subcommittee on Telemetering. The October 1941 report on telemetering, supervisory control, and associated circuits is being revised. A technical session was sponsored at the winter meeting.

Joint Subcommittee on Instrument Transformers. This newly formed subcommittee has organized working groups to handle activities on current transformers, test code, and rating factors. That part of the new Standard ASA *C57* which deals with instrument transformers is being published separately by the AIEE.

Joint Subcommittee on Standard Frequency Bands and Designations. A proposed table of standard frequency bands was published in *ELECTRICAL ENGINEERING*. Co-ordination with IRE, looking toward an ASA Standard, now is required.

COMMITTEE ON NUCLEONICS

Three meetings of the committee were held—one each in Montreal, Chicago, and Pittsburgh. At the Chicago meeting, the committee sponsored a talk by Doctor Enrico Fermi on the "Future of Atomic Power." The Des Moines District meeting had one session with four papers devoted to the subject of particle accelerators. The Pittsburgh meeting had a session of five conference papers on nucleonics instruments.

A series of papers on nucleonics were sponsored by the committee for publication in *ELECTRICAL ENGINEERING* as follows:

1. "The Atom and Its Nucleus," J. J. Smith (*EE, Dec '47, pp 1165–75*)
2. "The Relation Between Energy and Mass," Frederick Seitz, Jr. (*EE, Jan '48, pp 1–5*)
3. "The Nucleus—Its Structure and Reactions," W. E. Shoupp, Hugh Odishaw (*EE, Feb '48, pp 125–34*)
4. "Nuclear Engineering," E. U. Condon (*EE, Mar '48, pp 229–32*)
5. "Detection and Measurement of Nuclear Radiation," G. Wesley Dunlap (*EE, Apr '48, pp 325–32*)
6. "The Separation of Isotopes," G. Wesley Dunlap, R. M. Lichtenstein (*EE, May '48, pp 469–74*)
7. "Cascades Used in Isotope Separation," Harold C. Urey (*EE, Jun '48, pp 581–6*)
8. "Nuclear Reactors," F. L. Friedman (*EE, Jul '48, pp 685–93*)
9. "High-Energy Particle Accelerators," J. R. Woodyard (*EE, Aug '48, pp 759–67*)

The following articles are planned to complete the series:

10. "Physical and Medical Aspects of Radiation Hazards and Protection," L. L. German, H. M. Rosendaal
11. "Isotopic Tracers," J. T. Wilson
12. "Nuclear Reactors for Power Generation," W. F. Davidson

The purpose of this series is to furnish members of the Institute with an over-all picture

of recent developments in nucleonics and allied fields. It is planned to issue these articles in booklet form when the series is complete.

Jointly with the instruments and measurements committee of the Institute, a subcommittee on nucleonic instruments and controls was set up with Doctor G. W. Dunlap as chairman. This committee has held three meetings. It sponsored the session on nucleonics instruments referred to in the foregoing at the Pittsburgh meeting. It is planning additional sessions at the Washington and Spokane meetings.

Recently two additional subcommittees have been set up—one on nuclear physics with J. T. Wilson as chairman, and one on isotopic tracers with W. E. Barbour as chairman.

Another subcommittee on health safety is being planned jointly with the AIEE committee on therapeutics.

There is considerable activity not only in AIEE, but also in other professional organizations with regard to the development of a glossary of nuclear terms. This activity is being followed by the committee as it is desirable that such a glossary fill the needs in the various other technical fields, in addition to that of electrical engineering.

General Applications Group

COMMITTEE ON AIR TRANSPORTATION

The committee on air transportation held one meeting of the entire committee during the administrative year. This meeting was held in Dayton, Ohio, on September 26, 1947, following the Middle Eastern District meeting. At this meeting the Standards activities of the air transportation committee for the coming year were discussed and plans were made for technical sessions at future AIEE meetings.

The wide geographical spread of the aircraft industry has made active participation of interested groups in committee activities very difficult in the past. However, it is believed that this problem partially has been solved by the encouragement of the activities of air transportation divisions of the local Sections. In this way air transportation members may represent more effectively the majority opinion of the aviation industry when attending committee meetings.

Due to changes in personnel, the work of the committee and its subcommittees has been delayed to some extent. However, effort has been made to carry on the Standards program of the committee and to initiate new Standards to meet the requirements of the industry. The status of the committee's work is described under the heading of its several subcommittees as given in the following paragraphs.

Aircraft Electric Systems. This subcommittee has prepared the "Report on Aircraft Electric System Guide," AIEE 750, which was published in July 1947. The "System Guide" was issued with some sections incomplete with the aim of adding the unfinished section as soon as the material is available. At the present the subcommittees are co-operating in compiling information for the incomplete sections, which information will be correlated by the aircraft electric systems subcommittee and submitted to the Standards committee for inclusion in the "guide." In its present form the "System Guide" contains considerable valuable information which will be helpful to the designers and operators of aircraft electric systems. To increase the general acceptance of standard practices in aircraft electrical engineering it is desirable that the "System Guide" have a wider dissemination.

Aircraft Electric Rotating Machinery. The "Proposed Test Code for D-C Aircraft Machines," AIEE report 800, was issued March 1947 for one year's trial use. At the present a survey is being conducted to determine the extent of revisions and additions desirable in this proposed Standard, and to determine the recommendations of the air transportation committee to the Standards committee for its publication as an AIEE Standard.

This subcommittee at present is engaged in a project to formulate a new Standard on "Rating Standards of D-C Aircraft Generators." It is expected that a preliminary draft of this Standard will be ready for review by the air transportation committee by the end of 1948.

Aircraft Electric Control Protective Device and Cable. This subcommittee has been given the following assignment:

1. Prepare a test code for circuit interrupting devices.
2. Prepare test code for voltage regulators.

To date the first draft of "Proposed Test Code for Aircraft Circuit Interrupting Devices (12- and 24-volt d-c systems)" has been submitted to the subcommittee members for consideration and discussion. It is expected that this soon will be submitted to the Standards committee with recommendation that it be issued as a proposed test code for one year trial.

A working group has been appointed by the subcommittee to prepare a test code for d-c carbon pile voltage regulators. This working group has held three meetings and is scheduled to meet again in July 1948, at which time it is expected that a preliminary draft of the proposed test code will be presented for the consideration of and discussion by the group.

Basic Principles of Altitude Ratings. This subcommittee is preparing an AIEE Standard on the basic principles of altitude rating of electric machinery. The subcommittee has held three meetings and has compiled considerable information on environmental condition of altitude up to 50,000 feet. A survey is being conducted to determine the correlation of data obtained in flight with that obtained in altitude chambers. The survey also is to include the recommendation of users of altitude chambers concerning the use and limitation of chambers.

Joint Subcommittee (Air Transportation and Electric Machinery) on Carbon Brushes. This subcommittee is engaged in preparing an AIEE Standard for the testing of carbon brushes and brush material. Working groups have been formed of the subcommittee members to carry on the necessary investigations and to formulate the several phases of the standard test code necessary for the evaluation of carbon brush material and the performance of brushes on commutators and collector rings. It is expected that a preliminary draft of this Standard will be available by the end of 1948.

The air transportation committee is co-operating with the A2 committee of the Society of Automotive Engineers on problems of mutual interest. Liaison has been established with the National Aeronautical Standards Committee and with the Committee on Aviation and Airport Fire Protection, and it is expected that mutual benefit to all parties will result from the greater co-operative effort to produce working Standards which will be useful to the aviation industry.

Plans are being made for several technical sessions on air transportation at the Middle Eastern District meeting to be held in Washington, D. C., on October 5, 6, and 7, 1948. The response to the request for technical papers on aircraft electric subjects has been very gratifying. Effort is being made to stimulate increased preparation of technical papers and it is expected that technical sessions will be planned for more than one AIEE meeting during the next year.

COMMITTEE ON DOMESTIC AND COMMERCIAL APPLICATIONS

The committee on domestic and commercial applications, during the war years, was quite inactive and the present committee has been attempting to stimulate interest in the form of papers and discussions in the very broad field covered by the scope of this committee.

During the past year a very successful symposium on the heat pump was held in conjunction with the winter meeting of the Institute and work is under way now for a symposium on automatic protection of domestic equipment at the next winter meeting.

A paper on the application of inherent overheating protective devices for motors is scheduled for presentation at the summer meeting and one or two papers for District meetings are contemplated. While this is, perhaps, a small amount of activity when the tremendous advances in appliances of all kinds are considered, it is hoped it will be the beginning of a greater recognition among engineers generally of the real contribution that is being made by the engineers working on smaller equipment and publicized heretofore primarily through acceptance by the public rather than by any knowledge of their work by their fellow engineers in other lines.

Several subcommittees of this committee that have been completely inactive have been discontinued on the theory that contributions from an engineering standpoint are the only real justification for the existence of subcommittees. An alert and active main committee is contemplated instead.

COMMITTEE ON LAND TRANSPORTATION

Two well-attended meetings of the committee were held during the past year; one in June 1947 in Montreal and one during the winter meeting in January 1948 in Pittsburgh. The following activities were discussed and organized at these meetings.

Technical Meetings. Three transportation sessions were held at the winter meeting. A symposium on railroad electrification included papers on cost reduction of power

for electrification, conversion and transmission costs, overhead distribution costs, and electric locomotives with identical basic components. This session was unusually well attended and there was considerable interested discussion. A second session on heavy traction included papers on Great Northern Railroad motive power shunt capacitor installation on the New Haven, checking the integrity of high-speed train brakes, and speed responsive devices on Diesel-electric locomotives. The third session was on light traction and included papers on trolley coach distribution system, electric vehicles in city transit, new Chicago rapid transit cars, and a-c air conditioning for railroad passenger cars. These sessions were all very well attended. Two transportation papers are scheduled for the summer general meeting.

Subcommittee Activities. The subcommittee appointed to review AIEE Standard 16, "Standards for Electric Railway Control Apparatus," submitted an excellent report in June 1947. The committee voted to include in this Standard the electric portions of electropneumatic brakes. Representatives of two air brake companies now have been added to the subcommittee and a final report should be submitted in the near future.

The subcommittee engaged in gathering and tabulating heavy traction electrification data submitted questionnaires to the committee meeting in January and these questionnaires now are being duplicated and sent to the electrified railroads of the United States.

COMMITTEE ON MARINE TRANSPORTATION

Revisions of AIEE Standard 45 (Recommended Practice for Electric Installations on Shipboard), which is under the cognizance of this committee, have been completed.

The review of Standard 45 was completed in May 1947 and the results of the review were compiled, sorted to their appropriate heading, and referred to the committee's subcommittee chairmen for comparison with the existing Standard and further comments.

Subcommittee chairmen completed their work during the latter months of 1947 and submitted their comparisons and comments to the committee's editing subcommittee.

A meeting of the full committee was held February 27, 1948, at which meeting the work of the subcommittees was reviewed and with slight modifications, mutually agreed on, accepted.

A meeting of the committee's subcommittee chairmen was held April 15–16, 1948, with a representative of United States Coast Guard for the purpose of clarifying certain items of the proposed revisions.

Revision of Standard 45 in its final form has been prepared and submitted to the AIEE Standards committee for consideration.

COMMITTEE ON PRODUCTION AND APPLICATION OF LIGHT

The committee on the production and application of light arranged for a program for the Midwest general meeting in Chicago, Ill., on Tuesday, November 5, 1947. Technical papers were presented covering modern sources of infrared, visible, and ultraviolet radiation.

To encourage the planning of meetings on the subject of the production and application of light in various Sections, a letter was sent to all AIEE Section chairmen asking for a report on the number and type of such meetings which had been conducted within recent years, and offering the assistance of the committee in arranging such programs should they be desired. A number of the Sections requested assistance in arranging these programs, and such requests were referred to a member of the committee in the general area from which the request originated.

The remaining activities of the committee consisted of the formulation and continuation of certain subcommittee activities summarized in the following:

Subcommittee on Nomenclature of Electronic Lamps. This subcommittee was formulated several years ago at the request of the committee on electronics to assist in the preparation of definitions in the field of electronics by formulating definitions of lamps of this type.

Subcommittee on Preferred Voltage Characteristics of Electric Circuits for Lighting. A report from the previous chairman recommended that this particular subcommittee be discontinued. A copy of the report in which this recommendation was made is being circulated to various members of the committee to determine whether or not this recommendation will be followed.

Subcommittee on Infrared Radiation. A subcommittee to deal specifically with the subject of infrared radiation has been formulated and a program set up whereby it will work in close co-operation with the subcommittee on radiant heating of the technical committee on electric heating.

Subcommittee on Ultraviolet Radiation. A similar subcommittee was formulated to cover the field of the production and application of ultraviolet radiation.

The latter two subcommittees are working on specific assignments connected with the preparation of papers covering their specific fields.

Awards

INSTITUTE PRIZES

Five Institute prizes and ten District prizes were awarded for papers presented during the calendar year 1947, and for graduate and undergraduate student papers presented during the academic year ending June 30, 1947. A number of the papers were of a high order and the choice between leading papers in the several classes of Institute prizes was very close, so that six other papers received honorable mention.

Changes in prize awards for technical papers were announced in *ELECTRICAL ENGINEERING*, December 1947, pages 1248–9. Although it is too early to observe the effectiveness of these changes at the time of writing this report, 17 Branch prizes already have been awarded for the academic year ending July 30, 1948. This is a new prize established, one for each Branch, consisting of $10 cash plus an allowance of seven cents per mile one way for the winning representative to attend the District Branch competition.

CHARLES LEGEYT FORTESCUE FELLOWSHIP

This fellowship, which provides an allowance of $500 a year or more for graduate study in electrical engineering, was established as a memorial to Charles LeGeyt Fortescue in recognition of his valuable contributions to the electric power industry.

For the academic year 1948–49, a fellowship in the sum of $1,000 was awarded to James B. Woodford, Jr., a student at the Carnegie Institute of Technology, who will continue at that institution in graduate study.

COMMITTEE ON MEMBERS-FOR-LIFE FUND

The Members-For-Life fund committee held no meetings. By correspondence, it reviewed the use of the restricted fund and made recommendations to be embodied in report to President Hull, for board consideration at the summer general meeting.

EDISON MEDAL

The Edison Medal for 1947 was awarded to Doctor Joseph Slepian, associate director of the Westinghouse Research Laboratories, "For his theoretical and practical contributions to power systems through circuit analysis, arc control, and current interruption," and was presented on January 28, 1948, during the winter general meeting. The addresses given in connection with the presentation appear in *ELECTRICAL ENGINEERING* for March 1948, pages 258–64.

The medal may be awarded annually for meritorious achievement in electrical science, electrical engineering, or the electrical arts. Awards are made by a committee of 24 members of the Institute.

LAMME MEDAL

The Lamme Medal for 1947 was awarded to A. M. MacCutcheon, past president of the Institute, who retired, in 1946, from the position of vice-president in charge of engineering, Reliance Electric and Engineering Company, Cleveland, Ohio, "For his distinguished accomplishments in the development of motors for industrial needs, notably in the steel industry." The presentation will be made on June 23, 1948, during the summer general meeting in Mexico City.

Awards of the medal may be made annually by a committee of nine members of the Institute for meritorious achievement by a member of the AIEE in the development of electric apparatus or machinery.

HOOVER MEDAL

The Hoover Medal is awarded periodically to an engineer for distinguished public service by a board representing the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, and AIEE. No award was made for 1947.

JOHN FRITZ MEDAL

The John Fritz Medal may be awarded annually for notable scientific or industrial achievements, by a board of award composed of representatives of the ASCE, AIME, ASME, and AIEE. The 1948 medal was awarded to Doctor Theodor von Kármán as a "creative leader, stimulating teacher, and wise counselor in engineering and physical research in the fields of the aeronautical and structural sciences, for his many applications of mathematical and physical theory to the sound solution of engineering problems."

WASHINGTON AWARD

The Washington Award for 1948 was made to Senator Ralph E. Flanders of Vermont, who is president of the Jones and Lamson Machine Company, Springfield, Vt. The award is made annually to an outstanding engineer, a citizen or resident of the United States, who ably has served

human needs, and is administered by a commission representing the Western Society of Engineers, ASCE, AIME, ASME, and AIEE.

ALFRED NOBLE PRIZE

This prize was established in 1929 to perpetuate the name and achievements of Alfred Noble, past president of the ASCE and the Western Society of Engineers. It may be made to a member of any of the co-operating societies, ASCE, AIME, ASME, AIEE, or WSE, for a technical paper of particular merit accepted by the publication committee of any of these societies, provided the author is not over 30 years of age at the time of such acceptance.

Joint Activities

UNITED ENGINEERING TRUSTEES, INC.

This organization manages property and funds held jointly by the four Founder Societies, including the Engineering Societies Building, the Engineering Societies Library, and the endowment funds of the Engineering Foundation. It also acts as treasurer for the Engineers' Council for Professional Development.

An abstract of the annual report of UET for the year ended September 30, 1947, appears in *ELECTRICAL ENGINEERING*, February 1948, page 209.

ENGINEERING FOUNDATION

The Engineering Foundation, a department of United Engineering Trustees, was established in 1914 as a joint agency of the Founder Societies for "the furtherance of research in science and engineering."

As part of its program, the foundation supported 13 research projects during the year 1946–47. For the current year it is continuing the maintenance of nine of these projects, and has added eight new ones.

An abstract of the annual report for the year ended September 30, 1947, appears in *ELECTRICAL ENGINEERING*, February 1948, pages 210–11.

ENGINEERING SOCIETIES LIBRARY

The Engineering Societies Library, formed by combining the separate libraries of the ASCE, AIME, ASME, and AIEE, is conducted as a free public reference library.

Total resources of the library presently include 164,051 volumes, 10,316 maps, and 4,943 searches. The library also renders special services such as translations, photoprints, bibliographies, and book loans by mail.

An abstract of the annual report for the year ended September 30, 1947, appears in *ELECTRICAL ENGINEERING*, February 1948, pages 209–10.

ENGINEERS' COUNCIL FOR PROFESSIONAL DEVELOPMENT

The principal activities of ECPD include "programs for the guidance of young persons thinking of entering the engineering field, the accrediting of curricula of engineering schools, encouragement and assistance to individuals in their engineering and cultural studies during several years following graduation, and the development of criteria for indicating the attainment of the status of an engineer." The council represents the national societies of chemical, civil, electrical, mechanical, and mining and metallurgical engineers. The Engineering Institute of Canada, the American Society for Engineering Education, and the National Council of State Boards of Engineering Examiners.

A report of ECPD's activities during the year 1946–47 is included in the annual report of the Engineering Foundation (*EE, Feb '48, p 211*).

ENGINEERING SOCIETIES PERSONNEL SERVICE, INC.

Operating as an incorporated nonprofit organization, the Engineering Societies Personnel Service, Inc., assists members of the Founder Societies desiring to secure new positions and employers searching for engineers. Nonmembers may receive such assistance when positions available cannot be filled by members.

Offices are operated in New York, N.Y.; Detroit, Mich.; Chicago, Ill.; San Francisco, Calif.; with the co-operation of the Engineering Society of Detroit in that city, the Western Society of Engineers in Chicago, and the Engineers Club of San Francisco in that city.

ENGINEERS JOINT COUNCIL

This council, formerly known as the joint conference committee, is composed of the presidents, immediate past presidents, and secretaries of the Founder Societies and the AIChE. Its functions are to consider matters of interest to the societies and to make recommendations to their governing bodies.

REPRESENTATIVES

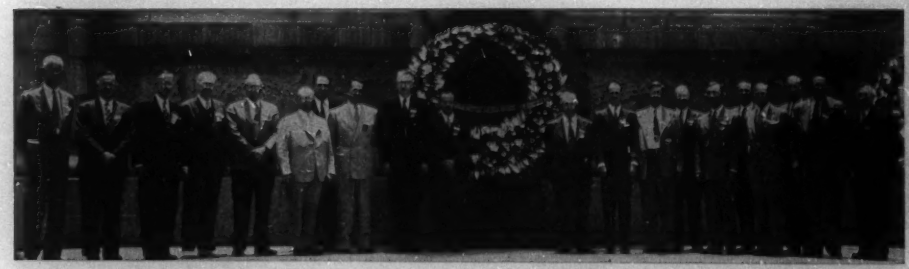
A complete list of about 40 joint bodies upon which the Institute is represented appears in the Year Book and in the October issue of *ELECTRICAL ENGINEERING* each year.

Appreciation

The board of directors acknowledges with appreciation and thanks the splendid co-operation received from Institute committees, District, Section, and Branch officers, and the membership.

Respectfully submitted for the board of directors.

H. H. HENLINE
Secretary



Official group of AIEE national officers and local representatives, acting as a special guard of honor, placed a memorial wreath at the Perpetual Flame in the base of the Monument to the Heroes of the Mexican Independence, June 22, 1948, during the 1948 AIEE summer general meeting. Left to right: Director J. M. Flanigen of Atlanta, Ga.; Vice-President E. W. Davis of Cambridge, Mass.; Director J. F. Fairman of New York City; Vice-president J. H. Berry of Norfolk, Va.; Vice-President C. F. Terrell of Seattle, Wash.; Past-President J. Elmer Housley of Alcoa, Tenn.; Jorge Rivera S. of the hospitality committee; Hector Martinez D'Meza, vice president of the summer general meeting committee; Secretary H. H. Henline of New York; Oscar R. Enriquez of Mexico City, president of the summer general meeting committee; AIEE President Blake D. Hull of Dallas, Texas; Manuel M. de Lascurain of Mexico City, member of the executive group of the summer general meeting committee; AIEE President-Elect Everett S. Lee of Schenectady, N. Y.; Director R. T. Henry of Buffalo, N. Y.; Director D. A. Quarles of New York, N. Y.; Vice-President-Elect Victor Siegfried of Worcester, Mass.; Vice-President D. G. Geiger of Toronto, Canada; Director E. P. Yerkes of Philadelphia, Pa.; Vice-President R. F. Danner of Oklahoma City, Okla.; Director Walter C. Smith of Palo Alto, Calif.

HASKINS & SELLS
CERTIFIED PUBLIC ACCOUNTANTS

1 EAST 44TH STREET
NEW YORK 17

May 19, 1948

American Institute of Electrical Engineers,
33 West 39th Street, New York.

Dear Sirs:

We have examined the balance sheet of the American Institute of Electrical Engineers, and schedule of securities owned, as of April 30, 1948, and the related statements of cash receipts and disbursements of operating and restricted funds for the year ended that date. Our examination was made in accordance with generally accepted auditing standards and included such tests of the accounting records and such other auditing procedures as we considered necessary in the circumstances.

In our opinion, the accompanying balance sheet, schedule of securities owned, and statements of cash receipts and disbursements, respectively, present fairly the financial condition of, and securities owned by, the Institute as of April 30, 1948, and its recorded cash receipts and cash disbursements for the year ended that date, in conformity with generally accepted accounting principles applied on a basis consistent with that of the preceding year.

Yours truly,

(*Signed*) HASKINS & SELLS

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Balance Sheet, April 30, 1948

Exhibit A

ASSETS

Property Fund Assets:		
One-fourth interest in physical properties of United Engineering Trustees, Inc.:		
Land, buildings, and equipment (less depreciation and renewal reserve)	$342,957.02	
Funded depreciation and renewal reserve	155,491.46	
Total	$498,448.48	
Equipment:		
Library (volumes and fixtures)—estimated value	36,366.37	
Office furniture and fixtures (less reserve for depreciation, $30,708.19)	15,019.07	
Works of art, etc.	3,001.35	
Cash (no change during year)	612.91	
Total property fund assets		$ 553,448.18
Restricted Fund Assets:		
Securities—at cost, less reserve, $1,788.15 (quoted market value, $555,516.78)—Schedule 1	$534,223.30	
Cash (Exhibit C)	39,398.08	
Accrued interest receivable	125.40	
Total restricted fund assets		573,746.78
Operating Fund Assets:		
Cash (not including $2,031.98 for Federal taxes withheld from employees)—(Exhibit *B*)	$22,394.58	
Accounts receivable:		
Members—for dues (less reserve, $4,500.00)	11,824.95	
Advertisers	1,449.25	
Miscellaneous	4,356.05	
Accrued interest receivable	8,450.55	
Inventories:		
TRANSACTIONS, etc.	1,216.00	
"Electrical Definitions"	1,409.40	
Work in process (May issue of *ELECTRICAL ENGINEERING*, etc.)	14,271.27	
Text and cover paper	17,592.46	
Badges	7,123.35	
Total operating fund assets		90,087.86
Total		$1,217,282.82

LIABILITIES

Property Fund Reserve		$ 553,448.18
Restricted Fund Reserves:		
Reserve capital fund	$544,847.61	
Life membership fund	8,636.75	
Member-for-Life fund	8,416.86	
International Electrical Congress of St. Louis library fund	6,400.11	
Lamme Medal fund	4,430.44	
Mailloux fund	1,015.01	
Total restricted fund reserves		573,746.78
Operating Fund Reserves, Liabilities, etc.:		
Accounts payable	$ 21,701.08	
Deferred income:		
Dues received in advance	3,390.00	
Entrance fees and dues advanced by applicants for membership	1,833.30	
Subscriptions to publications received in advance	18,998.34	
Miscellaneous (including unallocated receipts)	1,306.14	
Operating fund reserves	42,859.00	
Total operating fund reserves, liabilities, etc.		90,087.86
Total		$1,217,282.82

Statement of Cash Receipts and Disbursements of Operating Fund for the Year Ended April 30, 1948

Exhibit B

Cash on Deposit with The National City Bank of New York, May 1, 1947 (not including $1,664.75 for Federal taxes withheld from employees)		$ 6,883.37
Receipts:		
Dues (including $154,866.00 allocated to *ELECTRICAL ENGINEERING* subscriptions)	$332,459.02	
Advertising	128,817.86	
TRANSACTIONS subscriptions	10,651.82	
ELECTRICAL ENGINEERING subscriptions	31,941.83	
Miscellaneous publications (preprints, Standards, special reports, etc.)	41,976.23	
Students' fees	32,559.00	
Entrance fees	19,778.38	
Membership badges	4,913.15	
Transfer fees	3,448.75	
Interest and dividends on investments of Reserve Capital Fund	21,585.80	
Miscellaneous	33.38	
Total	$628,165.22	
Refund from Committee on Collective Bargaining and Related Matters	2,458.91	
Transfer from Reserve Capital Fund (see Exhibit C)	60,000.00	
Total receipts		690,624.13
Total		$697,507.50
Disbursements:		
Publications expense:		
ELECTRICAL ENGINEERING text	$172,956.32	
ELECTRICAL ENGINEERING advertising	71,069.15	$244,025.47
TRANSACTIONS		5,387.45
PROCEEDINGS		28,434.26
"Year Book"		9,542.29
Special publications		1,204.52
Miscellaneous publications (preprints, Standards, etc.)		36,784.77
Institute meetings		25,202.56
Institute Sections		73,673.21
Institute Branches		5,502.16
Finance committee		750.00
Headquarters committee		42.00
Membership committee		16,311.43
Standards committee		15,760.11
Technical committees		11,442.38
Committee on Institute publicity		900.00
Planning and co-ordination committee		1,686.48
Edison Medal committee		406.56
Forward	$477,055.65	
Total (forward)		$697,507.50

Total (forward)		$697,507.50
Disbursements (forward)	$477,055.65	
Traveling expenses:		
Geographical Districts:		
Executive committees	7,959.71	
Vice-presidents	1,289.79	
Conferences on Student activities	14,177.09	
Board of directors	12,329.57	
Nominating committee	1,355.09	
President's appropriation	3,088.65	
AIEE representatives	192.17	
Administrative expenses	87,390.11	
Geographical Districts—Branch paper prizes	368.34	
Institute prizes—national	500.00	
Retirement system AIEE—normal contribution	7,319.58	
American Co-ordinating Committee on Corrosion	50.00	
American Standards Association	1,125.00	
Canadian Radio Technical Planning Board	10.00	
Canadian Council of Professional Engineers and Scientists	213.82	
Engineers' Council for Professional Development	1,812.50	
Engineering Foundation Project—Welding research	250.00	
Engineers Joint Council—General expenses	564.90	
Hoover Medal	291.72	
John Fritz Medal	110.00	
National Committee State Board Engineering Examiners	500.00	
National Fire Protection Association—Dues	60.00	
National Science Foundation	50.00	
Radio Technical Planning Board	400.00	
United Engineering Trustees, Inc.:		
Building assessments	18,825.18	
Library assessments	12,699.50	
Representation on labor panel and incidental expenses	4,501.83	
Lamme Medal expense (see Exhibit C)	109.30	
Membership badges	6,577.37	
Legal services	250.00	
Office furniture and fixtures and repairs	4,589.06	
Headquarters repairs, etc.	641.62	
Rent, etc., 500 Fifth Avenue	6,427.18	
Miscellaneous	61.19	
Total	$673,145.92	
Transfer to Member-for-Life fund	1,967.00	
Total disbursements		$675,112.92
Cash on Deposit with The National City Bank of New York, April 30, 1948 (not including $2,031.98 for Federal taxes withheld from employees)		$ 22,394.58

Statement of Cash Receipts and Disbursements of Restricted Funds for the Year Ended April 30, 1948

Exhibit C

		Restricted Funds					
	Total	Reserve Capital Fund	Member-for-Life Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund
Cash on Deposit With The National City Bank of New York and Various Savings Banks, May 1, 1947	$ 23,488.62	$ 10,976.67	$6,908.41	$3,532.39	$756.19	$160.00	$1,154.96
Receipts:							
Proceeds from sales, etc., of securities	$ 90,983.47	$ 90,983.47					
Income from bonds	422.84			$ 127.56	$135.28	$160.00	
Interest on bank balances	63.09			46.80			$ 16.29
Transfer from operating fund	1,967.00		$1,967.00				
Life membership fees	504.68			504.68			
Total receipts	$ 93,941.08	$ 90,983.47	$1,967.00	$ 679.04	$135.28	$160.00	$ 16.29
Total	$117,429.70	$101,960.14	$8,875.41	$4,211.43	$891.47	$320.00	$1,171.25
Disbursements:							
Purchase of securities	$ 16,471.08	$ 16,471.08					
Transfers to operating fund:							
To be repaid when adequate funds become available	60,000.00	60,000.00					
Other	775.75			$ 775.75			
Purchase of medal, cost of engraving, etc. (exclusive of $109.30 paid from operating fund)	170.00					$170.00	
Library purchases	156.24						$ 156.24
Traveling expenses	458.55		$ 458.55				
Total disbursements	$ 78,031.62	$ 76,471.08	$ 458.55	$ 775.75		$170.00	$ 156.24
Cash on Deposit With The National City Bank of New York and Various Savings Banks, April 30, 1948	$ 39,398.08	$ 25,489.06	$8,416.86	$3,435.68	$891.47	$150.00	$1,015.01

Schedule 1

Securities Owned, April 30, 1948

	Principal Amount of Bonds or Number of Shares of Stock	Restricted Funds: Reserve Capital Fund	Life Membership Fund	International Electrical Congress Library Fund	Lamme Medal Fund	Total
Railroad Bonds:						
Atlantic Coast Line First Consolidated 4%, due 1952	$ 4,000.00				$4,253.78	$ 4,253.78
Baltimore & Ohio, Pittsburgh, Lake Erie & West Virginia System 4%, due 1980	10,000.00	$ 6,450.00				6,450.00
Chicago & Erie Railroad Company 5% first mortgage, due 1982, registered	1,000.00	1,105.00				1,105.00
New York Central Railroad Company 4% series *A* consolidated mortgage, due 1998	15,000.00	9,812.50				9,812.50
Northern Pacific Railway Company 4½% collateral trust, due 1975	15,000.00	15,307.50				15,307.50
St. Louis-San Francisco Railway Company 4% series *A*, first mortgage, due 1997	1,200.00	1,200.00				1,200.00
St. Louis-San Francisco Railway Company 4% series *A*, first mortgage, scrip	198.00	198.00				198.00
St. Louis-San Francisco Railway Company 4½% series *A*, second mortgage, income bonds, due 2022	1,300.00	1,300.00				1,300.00
St. Louis-San Francisco Railway Company 4½% series *A*, second mortgage, income bonds, scrip	61.10	61.10				61.10
Total railroad bonds		$ 35,434.10			$4,253.78	$ 39,687.88
Public Utility Bonds:						
American Telephone & Telegraph Company 2¾% convertible debentures, due 1961	$25,000.00	$ 17,163.50	$5,153.12	$5,457.85		$ 27,774.47
United States Government Bonds:						
Treasury Savings bonds series *D*, due July 1, 1949	$10,000.00	$ 7,644.00				$ 7,644.00
Treasury Savings bonds series *D*, due January 1, 1950	10,000.00	7,500.00				7,500.00
Treasury bonds 2%, due March 15, 1952/50	25,000.00	25,617.19				25,617.19
Defense bonds series *F*, due June 1, 1953	34,000.00	25,160.00				25,160.00
Defense bonds series *G*, 2½%, due December 1, 1954	18,000.00	18,000.00				18,000.00
Treasury Savings bonds series *G*, 2½%, due September 1, 1955	40,000.00	40,000.00				40,000.00
Treasury Savings bonds series *G*, 2½%, due November 1, 1956	17,000.00	17,000.00				17,000.00
Treasury Savings bonds series *G*, 2½%, due May 1, 1957	20,000.00	20,000.00				20,000.00
Treasury Savings bonds series *G*, 2½%, due October 1, 1957	30,000.00	30,000.00				30,000.00
Total United States Government bonds		$190,921.19				$190,921.19
Capital Stocks:						
American Can Company	60 shares	$ 4,988.40				$ 4,988.40
American Telephone & Telegraph Company	100 shares	17,341.65				17,341.65
Atchison, Topeka & Santa Fe Railway Company preferred	200 shares	19,174.71				19,174.71
Boston Edison Company	200 shares	4,927.50				4,927.50
Commonwealth Edison Company	200 shares	7,580.68				7,580.68
Consolidated Natural Gas Company	100 shares	4,428.80				4,428.80
Dow Chemical Company $4 cumulative preferred, series A	100 shares	11,547.50				11,547.50
Eastman Kodak Company	(*a*) 300 shares	10,694.11				10,694.11
E. I. du Pont de Nemours & Company	80 shares	13,096.68				13,096.68
General Electric Company	200 shares	7,748.66				7,748.66
General Motors Corporation $5 preferred	200 shares	25,820.00				25,820.00
General Motors Corporation	100 shares	4,235.53				4,235.53
Gulf Oil Corporation	200 shares	12,264.20				12,264.20
Insurance Company of North America	100 shares	10,847.50				10,847.50
International Harvester Company	100 shares	5,030.50				5,030.50
International Match Realization Company, Ltd., voting trust certificates for capital shares of International Match Corporation	6 shares*	1,788.15*				1,788.15*
Louisville & Nashville Railroad Company	100 shares	6,278.13				6,278.13
Ohio Edison Company 4.40% preferred	200 shares	21,279.25				21,279.25
Pacific Gas and Electric Company	200 shares	8,420.99				8,420.99
Public Service Corporation of New Jersey 6% preferred	100 shares	11,150.00				11,150.00
St. Louis-San Francisco Railway Company voting trust certificates for 5% series *A* preferred stock	.21 1800/10000ths shares	582.75				582.75
St. Louis-San Francisco Railway Company voting trust certificates for common stock	.42 6000/10000ths shares	357.00				357.00
Scoville Manufacturing Company 3.65% cumulative preferred	100 shares	10,111.25				10,111.25
Sears, Roebuck and Co.	400 shares	6,014.97				6,014.97
Standard Oil Company of Indiana	200 shares	8,170.32				8,170.32
Standard Oil Company of New Jersey	200 shares	9,601.26				9,601.26
Union Carbide & Carbon Corporation	100 shares	7,277.42				7,277.42
United Fruit Company	300 shares	11,985.00				11,985.00
United States Steel Corporation 7% cumulative preferred	100 shares	14,885.00				14,885.00
Total capital stocks		$277,627.91				$277,627.91
Total		$521,146.70	$5,153.12	$5,457.85	$4,253.78	$536,011.45
Less reserve in full for securities considered to be of doubtful value		1,788.15				1,788.15
Total Securities, Less Reserve		$519,358.55	$5,153.12	$5,457.85	$4,253.78	$534,223.30

a) Stock was split, effective May 15, 1947, by issuance of five shares for each share previously held.

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Schenectady, N. Y.
(Term expires July 31, 1949)

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Technical committees
Committee on education
Committee on planning and co-ordination
Committee on research
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Professional Group Co-ordinating Committee

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W. Scott Hill — J. R. North
Chairmen of professional group committees

Charles LeGeyt Fortescue Fellowship

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H. N. Muller, Jr. — H. P. Sedwick (*Terms expire July 31, 1950*)
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C. V. Christie — Zay Jeffries — Harold Pender (*Terms expire July 31, 1950*)
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Marvin W. Smith — H. S. Osborne — H. A. Winne (*Terms expire July 31, 1952*)
D. I. Cone — John B. MacNeill — Philip Sporn (*Terms expire July 31, 1953*)

Elected by the board of directors from its own membership for term of two years

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R. T. Henry — M. D. Hooven — Victor Siegfried (*Terms expire July 31, 1950*)

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(*Continued on page 1786*)

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W. L. Cassell — Claire W. Ricker
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K. B. McEachron, Jr. — E. A. Walker
T. G. McFarland — R. W. Warner
H. N. Muller, Jr. — S. Reid Warren

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F. E. Harrell — D. A. Quarles — J. B. Thomas (*Terms expire July 31, 1950*)
J. H. Berry — T. H. Morgan — L. A. Umansky (*Terms expire July 31, 1951*)

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K. P. Applegate — A. C. Monteith
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R. D. Bennett — H. A. Peterson
H. K. Breckenridge — D. A. Quarles
F. W. Bush — F. M. Roberts
E. E. Hill — O. Van Rye

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Power Co-ordinating Committee

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Ex-officio
Chairmen of the 11 following technical committees in the power group

Carrier Current

L. F. Kennedy, *chairman*, General Electric Company, central station engineering divisions, 1 River Road, Schenectady 5, N. Y.

(*Continued in next column*)

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W. H. Blankmeyer — L. E. Ludekens
C. W. Boadway — D. M. MacGregor
J. M. Dunham — T. W. MacLean
L. G. Eaton — R. H. Miller
R. C. Ericson — R. E. Neidig
L. F. Ferri — F. M. Rives
W. J. Googe — Merle Robison
K. E. Hapgood — Trueman T. Smith
E. W. Kenefake — Oliver A. Starcke
S. C. Leyland — T. D. Talmage
Mead Warren, Jr.

CARRIER CURRENT SUBCOMMITTEES

1. Project—Preparation of a General Interest Paper on Carrier Current

Scope: This paper is to give a historical review of the carrier developments and outline the place the carrier has in present-day system operating practices.

J. D. Booth, *chairman* — L. F. Kennedy
S. C. Bartlett

2. Project Committee Report on Application Guidance for Carrier Current

D. M. MacGregor, *chairman* — T. D. Talmage
C. W. Boadway

3. Working Group to Prepare Suggested Methods of Measurement

Scope: Establishment of methods of measurements in the carrier field.

E. W. Kenefake, *chairman* — C. W. Boadway
S. C. Bartlett — M. H. Wood

4. Working Group on High-Frequency Characteristics of Power Equipment

S. C. Leyland, *chairman* — T. A. Cramer

5. Working Group for Data on Carrier-Current Characteristics of Transmission Lines and Systems

Scope: Power system measurements at carrier frequency dealing primarily with transmission lines and substations.

R. C. Ericson, *chairman* — W. J. Googe
W. H. Blankmeyer — L. E. Ludekens
T. D. Talmage

6. Working Group on Standard Terminology for Carrier Current

S. C. Leyland, *chairman*

7. Working Group on Coupling Capacitors and Traps for Rural Line Carrier Telephone

S. C. Bartlett, *chairman*

8. Working Group on Use of Microwave Equipment for Relaying Telemetering and Supervisory Control

S. C. Leyland, *chairman*

9. Working Group on Operating Experience with Carrier Current Relaying Channels

O. A. Starcke, *chairman*

Insulated Conductors

L. F. Hickernell, *chairman*, Anaconda Wire and Cable Company, Hastings-on-Hudson 6, N. Y.
G. W. Acock — Glen T. Koger
C. F. Avila — L. I. Komives
H. G. Barnett — M. J. Lowenberg
C. E. Baugh — C. G. Mansfield
A. Bodicky — R. E. Morse
A. S. Brookes — E. F. Nuetzel
T. J. Brosnan — W. T. Peirce
W. R. Bullard — R. S. Peterson
H. W. Clark — A. D. Pettee
M. T. Crawford — H. S. Phelps
Alan S. Dana — R. A. Schatzel
E. W. Davis — G. B. Shanklin
W. A. Del Mar — V. A. Sheals
C. D. Eash — H. D. Short
D. M. Farnham — F. V. Smith
G. H. Fiedler — L. G. Smith
L. E. Fogg — G. M. L. Sommerman
J. H. Foote — E. G. Sturdevant
M. W. Ghen — Gordon Thompson
H. Halperin — C. W. White
C. T. Hatcher — R. J. Wiseman
T. G. Hieronymus — K. S. Wyatt
R. A. Hopkins — C. W. Zimmerer

INSULATED CONDUCTORS SUBCOMMITTEES

1. Administration Subcommittee

Scope: The activities of this subcommittee include subcommittee organization and scopes, co-ordination of subcommittee activities, program planning, and selection of papers for national meetings.

Main Committee Officers:
L. F. Hickernell, *chairman* — C. T. Hatcher, *secretary*
H. Halperin, *vice-chairman* — R. J. Wiseman

Subcommittee Chairmen:
A. S. Brookes — G. T. Koger
W. A. Del Mar — L. I. Komives
G. H. Fiedler — W. T. Peirce
J. H. Foote — R. S. Peterson
M. W. Ghen — G. M. L. Sommerman
Gordon Thompson

2. Standards and Publications Subcommittee

A. S. Brookes, *chairman* — J. H. Foote
A. P. S. Bellis — V. A. Sheals
L. E. Fogg — C. W. Zimmerer

Projects:
(2-1). Standard number 30 (1944)—revision
(2-2). Insulated Conductor Reference Book

3. Cable Characteristics Subcommittee

W. A. Del Mar, *chairman* — M. H. McGrath
H. Halperin — J. H. Neher
C. T. Hatcher — G. B. Shanklin
C. G. Mansfield — R. J. Wiseman — F. V. Smith

Projects:
(3-1). Current-carrying capacity of buried cables
(3-2). Zero-sequence impedance of cables
(3-3). A-c resistance of cables in conduit (paper)
(3-4). Reactance of cables in conduit (paper)
(3-5). Use of reduced-scale models for the solution of cable problems
(3-6). Hydraulic aspect of oil-filled pipe cables
(3-7). Movement of buried cable

4. Conductors Subcommittee

H. Foote, *chairman*

Projects:
(4-1). Determination of conductor resistivity
(4-2). Definition of terms relating to conductors
(4-3). *NBS* circular number 31—revision

5. Insulations Subcommittee

G. M. L. Sommerman, *chairman* — E. W. Greenfield
J. C. Balsbaugh — J. D. Piper
C. E. Betzer — R. A. Schatzel
F. M. Clark — A. J. Warner
A. S. Dana — K. S. Wyatt

Projects:
(5-1). Properties of synthetic rubber and plastics

6. Sheaths and Coverings Subcommittee

G. H. Fiedler, *chairman* — A. D. Pettee
C. E. Betzer — H. S. Phelps
A. S. Brookes — V. A. Sheals
F. E. Kulman — G. W. Zink

Projects:
(6-1). Properties of lead and lead alloy sheathing
(6-2). Properties of nonmetallic sheaths
(6-3). Protective covering for lead sheathing
(6-4). Evaluation of simulated service tests on lead sheathing

7. Cable Supply Systems Subcommittee

L. I. Komives, *chairman* — C. T. Hatcher
G. F. Avila — A. D. Pettee
H. G. Barnett — C. S. Schifreen
T. J. Brosnan — G. B. Shanklin
W. R. Bullard — H. D. Short
H. W. Clark — C. W. White
D. M. Farnham — R. J. Wiseman
H. Halperin — K. S. Wyatt

Projects:
(7-3). 69-kv cable installations at Philadelphia (paper)
(7-5). 69-kv cable installation at Milwaukee (paper)
(7-6). Nonmetallic sheathed cable in ducts for primary distribution
(7-7). Nonmetallic sheathed cable in ducts for secondary distribution
(7-8). Buried cable for distribution systems
(7-9). Aerial cable for distribution systems
(7-10). 69-kv medium-pressure gas-filled cable installation at Washington, D. C.
(7-11). Low-pressure gas-filled cable installations
(7-12). Paper cable with reduced insulation, short lays, soft binders, and so forth
(7-13). Economics of various types of extra-high-voltage cable systems
(7-14). Low versus high-viscosity oil for gas-pressure cable

8. Utilization Wiring Systems Subcommittee

G. T. Koger, *chairman* — L. C. Peterman
E. L. Bailey — Kennard Pinder
H. G. Barnett — S. J. Rosch
Allan Coggeshall — R. A. Schatzel
C. A. Finkel — E. G. Sturdevant
M. G. Houghton — C. V. Waddington
M. J. Lowenberg — C. W. Zimmerer
J. B. MacLean — G. W. Zink

Projects:

(8-1). National Electric Code—Problems affecting its application

(8-2). Wiring systems for corrosive and hazardous locations

(8-3). Metallic versus nonmetallic wiring

(8-4). Insulated aluminum conductors for utilization voltages

(8-5). The case for the bare neutral

(8-6). Conduit as a return conductor

9. Special Purpose Cables Subcommittee

W. T. Peirce, *chairman* — R. A. Hopkins
G. W. Acock — G. H. Landis
J. F. Atkinson — M. J. Lowenberg
M. T. Crawford — H. S. Moore
E. W. Davis — F. V. Smith
S. F. French — Myron Zucker — W. N. Zippler

Projects:

(9-1). Standard number 45—sections 18, 19, 20 (1945) —revision

(9-2). American versus foreign shipboard cable practice

(9-3). Mining cable

(9-4). Vertical-riser cable

(9-5). Generator lead cable

10. Accessories Subcommittee

M. W. Ghen, *chairman* — H. E. Martin
C. Christianson — R. E. Morse
C. D. Eash — J. H. Nicholas
T. G. Hieronymus — E. F. Nuezel
J. E. Johnson — F. P. West
L. Wetherill

Projects:

(10-1). Standard number 48 on cable potheads (1948) —discussion

(10-2). Termination of nonmetallic sheathed cables

(10-3). Development and technique of soldered porcelain potheads

(10-4). Interchangeability of potheads

(10-5). Molded joints for polyethylene-insulated coaxial cable

(10-6). Standard on cable limiters

(10-7). Effect of external bushing contamination on flashover value of potheads

(10-8). Fundamental design characteristics of potheads

(10-9). Aerial-cable accessories

11. Structures Subcommittee

R. S. Peterson, *chairman* — C. D. Eash
C. E. Baugh — G. H. Fiedler
Andrew Bodicky — M. W. Ghen
T. J. Brosnan — C. G. Mansfield
H. W. Clark — L. G. Smith
H. L. Davis, Jr. — F. P. West

Projects:

(11-1). Conduits

(11-2). Gases in underground structures

(11-3). Transformer manhole and vault design and ventilation

(11-4). Manhole construction

(11-5). Supports for aerial cable

(11-6). Supports for bridge cable

(11-7). Under river crossings

(11-8). Submarine cables and methods of support

(11-9). Cable installations in tunnels

12. Tests and Measurements Subcommittee

Gordon Thompson, *chairman* — E. S. Halfmann
J. C. Balsbaugh — F. K. Harris
A. L. Brownlee — J. C. Parker
R. W. Chadbourn — H. D. Short
A. S. Dana — G. M. L. Sommerman
E. W. Davis — E. R. Thomas
W. A. Del Mar — R. J. Wiseman
E. W. Greenfield — C. W. Zimmerer

Projects:

(12-1). Test code for insulated conductors

(12-2). Equivalent 1-hour life for oil-filled and gas-filled cable

(12-3). High-frequency measurements

(12-4). A-c resistance and reactance measurements

(12-5). Impulse strength testing

(12-6). Practice in testing of installed cables

(12-7). Determination of tensile strength of lead sheathing

Power Generation

A. H. Frampton, *chairman*, English Electric Company of Canada Ltd., St. Catherines, Ont., Canada

H. A. Adler — W. R. Johnson
R. E. Appleyard — L. A. Kilgore
J. E. Barkle — A. J. Krupy
S. Beckwith — F. L. Lawton
J. A. Brooks — W. J. Lyman
G. Calabrese — J. T. Madill

(*continued in next column*)

A. D. Caskey — A. P. Maness
S. M. Denton — T. H. Mawson
E. F. Dissmeyer — J. B. McClure
Alex. Dovjikov — G. H. McDaniel
J. M. Drabelle — D. R. Pattison
J. Finlaw — W. S. Peterson
C. B. Fontaine — A. L. Pollard
C. A. Harrington — F. L. Reinmann
H. R. Harris — B. G. A. Skrotzki
A. P. Hayward — G. M. Tatum
B. V. Hoard — C. W. Watchorn
E. L. Hough — G. R. Woodman

POWER GENERATION SUBCOMMITTEES

1. Prime Movers Subcommittee

Scope: To study, review design, installation practices, and other features of all types of prime movers and their associated equipment (except hydraulic units) as related to electric power generation. To sponsor specifications, Standards, and technical sessions, the latter to be devoted principally to new developments or review of existing practices. To co-operate with other committees of organizations such as American Society of Mechanical Engineers and Edison Electric Institute.

B. G. A. Skrotzki, *chairman* — J. A. Brooks
H. A. Adler — J. Finlaw
S. Beckwith — H. R. Harris

2. Speed Governing Subcommittee

Scope: To study, review design, installation practices, and other features of all types of prime-mover governing systems, including hydroelectric. To sponsor specifications, Standards, and technical sessions and co-operate with other committees of the AIEE and other societies.

J. B. McClure, *chairman* — A. P. Hayward
C. P. Almon, Jr. — T. H. Mawson
R. E. Appleyard — G. H. McDaniel
R. M. Beville — F. L. Reinmann

3. Excitation Systems Subcommittee

Scope: Similar functions as for the speed-governing subcommittee in the field of generating-station excitation systems.

A. J. Krupy, *chairman* — A. Dovjikov
J. E. Barkle, Jr. — C. B. Fontaine, Jr.
E. F. Dissmeyer — L. A. Kilgore
J. B. McClure

4. Station Design Subcommittee

Scope: To review designs and installation practices in electric generating stations considered as a unit. To sponsor technical sessions covering new or improved practices. To analyze trends and designs and present general interest material of future possibilities. This subcommittee will not cover hydroelectric stations

A. D. Caskey, *chairman* — E. L. Hough
J. M. Drabelle — W. R. Johnson
C. A. Harrington — D. R. Pattison

5. Hydroelectric Systems Subcommittee

Scope: Combining the scope, for hydroelectric station only, of the prime movers and station design subcommittees.

W. S. Peterson, *chairman* — J. T. Madill
S. M. Denton — A. P. Maness
B. V. Hoard — A. L. Pollard
F. L. Lawton — G. R. Woodman
L. B. Stirling

6. Pacific Coast Subcommittee

Scope: To foster closer co-operation with the western members in all projects undertaken by the main committee. To provide direct contact in the planning of Pacific Coast technical meetings.

A. Dovjikov, *chairman* — W. R. Johnson
S. M. Denton — W. S. Peterson
B. V. Hoard — A. L. Pollard
G. R. Woodman

Protective Devices

G. B. Dodds, *chairman*, Duquesne Light Company, 435 Sixth Avenue, Pittsburgh 19, Pa.

V. J. Cisma — W. A. Lewis, Jr.
J. E. Clem — F. R. Longley
L. M. Connors — C. Lowerison
H. A. Cornelius — T. H. Mawson
F. M. Defandorf — C. McLean
R. H. Earle — E. G. Norell
W. W. Eberhardt — A. M. Opsahl
G. W. Gerell — W. J. Rudge
E. M. Hunter — W. E. Rushlow
T. I. Eldridge — O. L. Sidenfaden
P. A. Jeanne — W. T. Smith
Alva A. Johnson — H. R. Stewart
J. H. Kinghorn — J. M. Towner
E. W. Knapp — S. S. Watkins
W. E. Knouse — E. H. Yonkers

PROTECTIVE DEVICES SUBCOMMITTEES

1. Lightning Protective Devices Subcommittee

H. R. Stewart, *chairman* — H. W. Hartzell
E. H. Yonkers, *secretary* — T. W. Mawson
H. A. Cornelius — J. R. McFarlin
F. M. Defandorf — W. A. McMorris
R. H. Earle — A. M. Opsahl
Milan Getting — W. E. Rushlow
I. W. Gross — A. H. Schirmer
E. H. Grosser — J. M. Towner
E. R. Whitehead

Project 1: Application guide of methods for lightning protection of substations.

H. A. Cornelius, *chairman* — S. B. Howard
E. H. Grosser — E. L. Jarrett
H. W. Hartzell — R. H. Mertz
A. M. Opsahl

Project 4: Lightning protection for unit substations.

E. R. Whitehead, *chairman* — E. M. Hunter
H. W. Hartzell — A. A. Johnson
J. M. Towner

Project 5: Revise and combine AIEE Standards 24, 28 and 47.

F. M. Defandorf, *chairman* — W. A. McMorris
R. H. Earle — A. M. Opsahl
I. W. Gross — W. E. Rushlow
E. H. Grosser — A. H. Schirmer
J. R. McFarlin — J. M. Towner
E. H. Yonkers

Project 6: Performance characteristics of station type arresters.

W. A. McMorris, *chairman*

Project 7: Performance characteristics of valve type distribution arresters.

J. R. McFarlin, *chairman* — T. I. Eldridge
R. H. Earle — W. A. McMorris
A. M. Opsahl

Project 8: Performance characteristics of 20 to 73 kv line type arresters.

R. H. Earle, *chairman*

Project 9: Joint working group (with general systems subcommittee of transmission and distribution committee) on recovery voltage characteristics of distribution systems.

R. L. Witzke, *chairman* — L. M. Olmstead
I. B. Johnson — M. A. Polster
W. E. Rushlow

Project 10: Performance characteristics of line expulsion arresters.

A. M. Opsahl, *chairman*

Project 11: Combined performance characteristics of lightning protective devices.

E. H. Grosser, *chairman* — Milan Getting
R. H. Earle — W. A. McMorris
J. R. McFarlin — A. M. Opsahl

Project 12: Status of rod gap characteristics.

Milan Getting, *chairman*

2. Fault Limiting Devices Subcommittee

A. A. Johnson, *chairman* — E. W. Knapp
J. E. Clem — W. A. Lewis
W. W. Eberhardt — F. R. Longley
E. T. B. Gross — W. E. Marter
H. W. Hartzell — E. G. Norell
E. M. Hunter — O. L. Sidenfaden
P. A. Jeanne — W. T. Smith

Project 1: Application guide on methods of grounding transmission systems.

E. M. Hunter, *chairman* — A. A. Johnson
H. W. Hartzell — R. E. Neidig

Project 2: Application guide on methods of grounding generator systems.

A. A. Johnson, *chairman* — W. E. Marter
E. M. Hunter — E. G. Norell
D. M. MacGregor — W. T. Smith

Project 3: Bibliography of literature on grounding devices.

E. T. B. Gross, *chairman* — P. A. Jeanne — W. A. Lewis

Project 4: Field survey of and application problems associated with ground fault neutralizers.

J. E. Clem, *chairman* — S. S. Cook
H. H. Brown — N. E. Dillon
B. H. Clingerman — W. D. Hardaway
R. E. Neidig

Project 5: Power system fault limitation.

F. R. Longley, *chairman* — L. B. LeVeaconte
J. O. R. Coleman — H. F. Lindemuth
F. H. Kierstead — E. L. Michelson
L. E. Sauer

Relays

W. R. Brownlee, *chairman*, The Commonwealth and Southern Corporation, Jackson, Mich.

R. J. Cooper	S. C. Leyland
R. E. Cordray	H. F. Lindemuth
G. E. Dana	J. R. Linders
G. B. Dodds	C. Lowerison
F. S. Fehr	W. E. Marter
L. F. Ferri	A. J. McConnell
L. L. Fountain	E. L. Michelson
S. Goldsmith	W. A. Morgan
E. T. B. Gross	C. E. Parks
T. R. Halman	H. R. Paxson
E. L. Harder	F. C. Poage
V. J. Hayes	E. G. Ratz
B. C. Hicks	K. N. Reardon
J. H. Kinghorn	H. P. Sleeper
R. A. Larner	W. K. Sonnemann
W. A. Lewis, Jr.	G. S. Steeb
E. C. Wentz	

RELAYS SUBCOMMITTEES

1. Project Committee on Bibliography of Relay Literature

E. T. B. Gross, *chairman*	C. E. Parks
S. Goldsmith	C. L. Smith

2. Project Committee on Generator Protection

H. R. Paxson, *chairman*	W. E. Marter
L. L. Draper	J. B. McClure
B. C. Hicks	A. J. McConnell
S. C. Leyland	C. E. Parks
H. F. Lindemuth	F. C. Poage
C. L. Smith	

3. Project Committee on Co-ordination of Construction and Protection of Distribution Circuits (Joint with Distribution Subcommittee and Edison Electric Institute)

G. B. Dodds, *AIEE sponsor* C. J. Beller, *SEI sponsor*

Relay Committee Appointees:	*Distribution Subcommittee Appointees:*	*EEI*
H. W. Haberl	R. O. Askey	G. H. Fiedler
C. L. Headley	T. J. Brosnan	W. McLean
C. Lowerison	W. B. Fisk	E. W. Knapp
C. E. Parks	A. L. Malmstrom	
H. P. Sleeper	H. F. Outman	
C. L. Smith	J. S. Parsons	
	R. F. Quinn	

4. Project Committee on Revision of Standards for Power Relays

S. C. Leyland, *chairman*	L. L. Fountain
R. E. Cordray	W. A. Lewis
V. L. Cox	E. L. Michelson

5. Project Committee on Sensitive Ground Protection

E. T. B. Gross, *chairman*	P. A. Jeanne
C. J. Berghs	C. Lowerison
R. J. Cooper	W. K. Sonnemann
T. R. Halman	A. R. van C. Warrington

6. Project Committee on Transmission Line Protection

E. L. Harder, *chairman*	W. E. Marter
L. J. Audlin	A. J. McConnell
W. R. Brownlee	M. S. Merritt
B. C. Hicks	E. L. Michelson
J. H. Kinghorn	C. E. Parks
W. A. Lewis	K. N. Reardon

7. Project Committee on Electronic Relay Applications

A. J. McConnell, *chairman*	E. L. Harder
J. E. Barkle	J. H. Kinghorn
R. J. Cooper	D. M. McGregor
F. S. Fehr	E. L. Michelson
E. G. Ratz	

8. Project Committee on Insulation Levels of Relay and Control Circuit

E. L. Michelson, *chairman*	T. R. Halman
R. E. Cordray	V. J. Hayes
S. Goldsmith	W. K. Sonnemann

9. Project Committee on Relaying of Interconnections Between Industrial and Utility Generating Systems

K. N. Reardon, *chairman*	F. S. Fehr
J. E. Barkle	T. R. Halman
G. E. Dana	J. R. Linders

10. Projection Committee on Relay Protection for Stations Without High-Voltage Switching

E. L. Michelson, *chairman*	V. J. Hayes
L. F. Ferri	R. A. Larner
L. L. Fountain	K. N. Reardon

11. Proposed Future Projects:

(*a*). Capacitance potential device transient performance as influencing relay operation.

(*b*). Review of bus protection methods and performance.

(*c*). Relay protection of circuits following sustained interruptions.

(*d*). A-c and low-voltage d-c tripping methods (joint).

(*e*). Relay test methods.

(*f*). Switching schemes influencing relay protection.

(*g*). Ultrahigh-frequency radio protective relay applications.

(*h*). Design and protection of pilot-wire channels.

(*i*). Uniform records of relay performance.

Rotating Machinery

C. E. Kilbourne, *chairman*, General Electric Company, turbine division, 1 River Road, Schenectady 5, N. Y.

J. E. Allen	A. A. Kroneberg
G. R. Anderson	T. M. Linville
W. R. Appleman	T. C. Lloyd
E. L. Bailey	M. R. Lory
S. Beckwith	C. Lynn
A. G. Conrad	G. W. McIver, Jr.
J. DeKiep	R. C. Moore
L. O. Dorfman	J. G. Noest
E. E. Dreese	E. I. Pollard
J. L. Fuller	C. P. Potter
J. S. Gault	F. C. Rushing
C. B. Hathaway	M. L. Schmidt
V. P. Hessler	R. V. Shepherd
L. E. Hildebrand	J. F. Sellers
T. A. Hopkins	H. R. Sills
W. R. Hough	T. W. Trice
J. W. Jones	P. H. Trickey
M. F. Kent	C. G. Veinott
C. Kingsley, Jr.	R. W. Wieseman

ROTATING MACHINERY SUBCOMMITTEES

1. Administrative Subcommittee

C. E. Kilbourne, *chairman*	E. I. Pollard
W. R. Hough	F. C. Rushing
T. M. Linville	M. L. Schmidt
J. L. Fuller	R. W. Wieseman

2. Synchronous Machinery Subcommittee

E. I. Pollard, *chairman*	E. L. Harder
Sterling Beckwith	R. A. Hopkins
J. F. Calvert	L. F. Hunt
S. B. Crary	J. W. Jones
J. DeKiep	C. E. Kilbourne
L. O. Dorfman	G. W. McIver
A. M. Goodison, Jr.	W. P. Monroe
J. G. Noest	

3. Induction Machinery Subcommittee

F. C. Rushing, *chairman*	C. J. Koch
E. L. Bailey	T. C. Lloyd
E. C. Barnes	R. C. Moore
B. M. Cain	J. G. Noest
S. F. Henderson	C. P. Potter
Charles Kingsley, Jr.	P. H. Trickey

4. D-C Machinery Subcommittee

T. M. Linville, *chairman*	C. B. Hathaway
W. E. Clancy	V. P. Hessler
F. A. Compton	P. Lebenbaum
Lanier Greer	C. Lynn
L. N. Grier	D. Ramadanoff
J. L. Hamilton	J. F. Sellers

5. Single-Phase and Fractional-Horsepower Subcommittee

M. L. Schmidt, *chairman*	R. F. Munier
W. R. Appleman	P. H. Trickey
B. M. Cain	C. G. Veinott
I. M. Levy	N. S. Yost

6. Test Code Subcommittee

J. L. Fuller, *chairman* T. E. M. Carville T. C. Lloyd

7. Insulation Resistance Subcommittee

R. W. Wieseman, *chairman*	D. D. Higgins
H. R. Stewart, *secretary*	J. W. Jones
J. E. Allen	F. M. Porter
L. J. Berberich	V. E. Schlossler
E. B. Curdts	J. B. Swering
E. F. Dissmeyer	A. L. Van Emden
Orville E. Fawcett	C. O. Weilbaecher

Substations

G. S. Lunge, *chairman*, General Electric Company, central station engineering divisions, 1 River Road, Schenectady 5, N. Y.

H. Bany	W. L. Hiscox
H. Barber	K. B. Hoffman

(*Continued in next column*)

R. O. Bell	E. M. Hunter
F. J. Berger	S. C. Killian
M. K. Brown	I. F. Krughoff
M. V. Eardley	W. J. Lank
R. C. Ericson	N. G. Larson
G. P. Fallon	J. T. Lienert
J. M. Flanigen	W. M. McCauley
M. Fraresso	M. E. Reagan
S. F. French	S. L. Smith
H. F. Gidlund	K. L. Wheeler
C. D. Highleyman	G. S. Whitlow

SUBSTATIONS SUBCOMMITTEES

1. Executive Subcommittee

Scope: To plan and co-ordinate the activities of the main committee and its subcommittees including consideration of their scopes, program planning and assistance in selection of papers.

Main Committee Officers:

G. S. Lunge, *chairman*
R. C. Ericson, *vice-chairman*
M. Fraresso, *secretary*

(*Subcommittee chairmen and personnel to be selected.*)

2. Transmission Substation Subcommittee

Scope: Responsible for treatment of all problems of structural and electrical design, construction and arrangement of these substations known as transmission substations.

(*Subcommittee chairmen and personnel to be selected.*)

3. Distribution and Conversion Substation Subcommittee

Scope: Responsible for treatment of all problems of structural and electrical design, construction and arrangement of those substations known as distribution substations.

(*Subcommittee chairmen and personnel to be selected.*)

4. Automatic and Supervisory Control Subcommittee

Scope: Responsible for treatment of all problems relating to the development of automatic, remote, and manual control, and also fundamental electrical diagrams pertaining to substations.

(*Subcommittee chairmen and personnel to be selected.*)

Switchgear

F. A. Lane, *chairman*, American Gas and Electric Service Corporation, 30 Church Street, New York 8, N. Y.

C. K. Beyette	W. J. McLachlan
Harry Brown	R. G. Meyerand
J. S. Brown	E. F. Miller
E. A. Childerhose	V. G. Muzzey
H. W. Codding	Otto Naef
V. L. Cox	J. R. North
G. H. Doan	H. V. Nye
W. E. Enns	M. W. Rothpletz
K. J. C. Falck	H. H. Rudd
R. M. Ferrill	O. L. Sidenfaden
J. M. Geiger	F. V. Smith
W. J. Gilson	G. Stamper
W. D. Hardaway	A. Van Ryan
V. J. Hayes	J. H. Vivian
M. H. Hobbs	R. L. Webb
L. M. Hovey	C. E. Winegartner
C. L. Killgore	J. D. Wood
H. A. P. Langstaff	J. C. Woods
H. J. Lingal	S. W. Zimmerman

SWITCHGEAR SUBCOMMITTEES

1. Circuit Breaker Subcommittee

R. L. Webb, *chairman*	H. V. Nye
F. W. Cramer	F. V. Smith
R. M. Ferrill	J. H. Vivian
W. D. Hardaway	K. L. Wheeler
M. H. Hobbs	J. D. Wood
W. J. McLachlan	J. C. Woods
J. R. North	B. W. Wyman

The following working groups have been set up within this subcommittee:

A—Power circuit breakers.
B—Low-voltage air circuit breakers.
C—Automatic reclosers.
D—Calculations of short-circuit currents in low-voltage systems (A. E. Anderson, *chairman*).

2. Switchgear Assemblies Subcommittee

H. V. Nye, *chairman*	G. M. Reed
F. W. Cramer	H. H. Rudd
J. M. Geiger	B. K. Sturgis
C. H. Kreger	O. B. Vikorem
E. F. Miller	C. P. West
W. F. Wetmore	

3. Switches, Fuses, and Insulators Subcommittee

H. H. Rudd, *chairman* — H. L. Rawlins
K. J. C. Falck — R. L. Webb
A. H. Powell — J. C. Woods

The following working groups have been set up within this subcommittee:

A—Air switches and insulators (H. L. Rawlins, *chairman*).

B—Interrupter switches (H. H. Powell, *chairman*).

C—Fuses (R. L. Webb, *chairman*).

4. Subcommittee on High Voltage Switching (Where Out of Phase Voltages Exist at Time of Arc Interruption)

K. J. Falck, *chairman* — I. W. Gross
W. H. Black — E. L. Harder
W. E. Enns — M. H. Hobbs
R. M. Ferrill — C. B. Kelley
G. D. Floyd — H. V. Nye

5. Incompleted Projects

The incompleted projects carried over from the protective devices committee of 1947–8 are:

(*a*). Completion of Standards for automatic reclosers for a-c distribution circuits.

(*b*). Revision of Switchgear Assembly Standard 27.

(*c*). Review Standards for low-voltage air circuit breakers, which were issued for a one year trial period in December 1946.

(*d*). Complete work on calculations of short-circuit currents for low voltage.

(*e*). Complete work on revisions of short-circuit calculation and application factors for power circuit breakers

(*f*). Complete test code for power circuit breakers.

(*g*). Revise AIEE Standard 19 (ASA *C-37*) to include temperature limits for oilless circuit breakers.

(*h*). Study rating factors for oil and oilless circuit breakers.

(*i*). Complete revisions of AIEE Standard 22 for air switches and bus supports.

6. New Projects

(*a*). Study of H. H. Rudd's proposed supplement to AIEE Standard 2, Guiding Principles for Impulse Insulation Co-ordination.

(*b*). Prepare a test code for low-voltage circuit breakers.

(*c*). Prepare Standards for automatic short-circuiting devices.

(*d*). Co-operate with ASA *C-42* Committee on revision of "Definitions of Electrical Terms."

System Engineering

E. Wild, *chairman*, Commonwealth Edison Company, 72 West Adams Street, Chicago 90, Ill.

C. G. Adams — A. P. Hayward
C. P. Almon, Jr. — J. B. Hodtum
John Bankus — R. G. Hooke
G. Bissett — L. F. Hunt
M. L. Blair — L. R. Janes
R. Brandt — Alva A. Johnson
O. A. Demuth — C. B. Kelley
A. G. Dewars — M. H. Lovelady
G. D. Floyd — O. W. Manz, Jr.
A. P. Fugill — V. M. Marquis
E. E. George — C. W. Mayott
A. C. Gohlke — S. B. Morehouse
G. H. Groh — C. B. Signor
E. T. B. Gross — W. O. Smith
W. D. Hardaway — M. L. Waring
H. L. Harrington — G. S. Whitlow

SYSTEM ENGINEERING SUBCOMMITTEES

1. Administrative Subcommittee

Scope: To serve as an executive committee in the assignment of work to subcommittees, to expedite work and to arrange for preparation and presentation of papers at meetings.

E. Wild, *chairman* — L. F. Hunt
R. Brandt — B. M. Jones
H. L. Harrington — C. W. Mayott
A. P. Hayward — O. W. Manz, Jr.
R. G. Hooke — C. N. Metcalf

2. System Planning Subcommittee

Scope: Investigation and study of load growth and generation requirements, interconnection of systems methods of transmission and interconnection, location and optimum sizes of units, and general system planning studies.

H. L. Harrington, *chairman* — L. R. Janes
C. G. Adams — A. A. Johnson

(*Continued in next column*)

R. Brandt — B. M. Jones
A. G. Dewars — A. A. Kroneberg
A. P. Fugill — H. P. St. Clair
E. T. B. Gross — C. B. Signor
W. D. Hardaway — N. N. Smeloff
J. B. Hodtum — M. Ward
R. G. Hooke — M. L. Waring

3. System Operations Subcommittee

Scope: Investigation and study of load and kilovar dispatching, spinning reserve, emergency procedures, resynchronizing procedures, requirements and usage of communication equipment, safety "hold-off procedures," functions of intersystem operating committees, and other general studies for improvement of system operation.

O. W. Manz, Jr., *chairman* — G. H. Groh
J. Bankus — L. F. Hunt
G. Bissett — C. B. Kelley
M. L. Blair — F. Oppenheimer
O. A. Demuth — H. W. Phillips
G. D. Floyd — W. O. Smith

4. System Economics Subcommittee

Scope: Investigation and study of interconnection operation, economics of long high-voltage power transmission, co-ordination of operation of units in a plant, between plants in a system, and between systems and other economic studies for improved system operation.

A. P. Hayward, *chairman* — R. T. Purdy
E. C. Brown — J. A. Rose
E. E. George — C. W. Watchorn
V. M. Marquis — A. L. Williams

5. System Controls Subcommittee

Scope: Investigation and study of load-frequency control equipment and operation, voltage control, phase angle control, and other general studies of control methods to improve system operations.

R. Brandt, *chairman* — A. C. Gohlke
C. G. Adams — A. A. Johnson
C. P. Almon, Jr. — C. N. Metcalf
D. H. Cameron — S. B. Morehouse
T. E. Curtis — R. E. Pierce
G. S. Whitlow

6. Interconnection Contracts Subcommittee

Scope: Investigation and study as to important features of interconnection contracts, economic interchange, emergency requirements and metering requirements, and other matters of interest to improve interconnection relations.

C. W. Mayott, *chairman* — N. B. Hinson
E. D. Early — C. B. Kelley
E. E. George — M. H. Lovelady
G. A. Grimm — V. M. Marquis
R. Martindale

Transformers

H. B. Keath, *chairman*, Wagner Electric Corporation, 6400 Plymouth Avenue, St. Louis 14, Mo.

J. A. Adams — A. A. Johnson
F. S. Brown — D. L. Levine
M. K. Brown — H. C. Louis
R. J. Brown — C. M. Lovell
J. L. Cantwell — V. M. Montsinger
J. H. Chiles — M. H. Pratt
J. E. Clem — T. D. Reimers
G. W. Clothier — W. C. Sealey
Merrill DeMerit — F. L. Snyder
J. A. Elzi — Paul A. Vance
M. M. Ewell — F. J. Vogel
I. W. Gross — C. V. Waddington
J. B. Hodtum — H. H. Wagner
W. G. James — C. E. Winegartner

TRANSFORMERS SUBCOMMITTEES

1. Working Group on Co-ordinated Study of Life of Transformer Insulation

V. M. Montsinger, *chairman*

2. Working Group on Routine Measurement of Hot Spot Temperature Rise

W. C. Sealey, *chairman*

3. Instrument Transformer Subcommittee

Transformer Committee Representatives:

J. E. Clem, *chairman* — D. R. Lalb
W. K. Dickenson — E. C. Wentz

Relay Committee Representatives:

R. E. Cordray — W. A. Lewis
T. R. Halman — W. E. Marter
W. K. Sonneman

Switchgear Committee Representative:

H. Bany — J. M. Geiger
K. J. C. Falck — C. A. Woods

Instruments and Measurements Committee Representatives:

J. H. Chiles — J. P. Scott
A. B. Craig — F. B. Silsbee

Transmission and Distribution Committee Representatives:

J. B. Hodtum — L. M. Moore

Carrier-Current Committee Representative:

L. F. Kennedy

Working Group Chairmen:

W. E. Marter, relaying performance of current transformers
E. C. Wentz, test code
L. F. Kennedy, rating factors

4. Working Group on Accuracy of Voltage Regulator Control Devices

A. A. Johnson, *chairman*

5. Working Group on Operating Guides for Regulators

A. A. Johnson, *chairman*

6. Working Group on Short-Circuit Thermal Ratings of Induction and Step-Type Regulators

A. A. Johnson, *chairman*

7. Working Group on Magnetization Characteristics of Transformers

J. E. Clem, *chairman*

8. Working Group on Revision of Dielectric Tests

F. J. Vogel, *chairman*

9. Working Group on Temperature Limits of Silicon Insulation Used in Transformers

V. M. Montsinger, *chairman*

10. Working Group on Proposed Revision of A.S.A. Rules in Connection with Short-Circuit Requirements of Transformers

V. M. Montsinger, *chairman*

11. Working Group on Care and Operation of Dry Type Transformers

J. A. Adams, *chairman*

12. Working Group on Co-ordination of Insulation

J. E. Clem, *chairman*

Transmission and Distribution

C. F. Wagner, *chairman*, Westinghouse Electric Corporation, central station engineering department, East Pittsburgh, Pa.

C. E. Baugh — E. K. Huntington
P. L. Bellasch — Arthur A. Jones
E. D. Bent — C. H. Kraft
D. K. Blake — A. A. Kroneberg
B. Brady — J. T. Lusignan
T. J. Brosnan — D. W. Nethercut
H. E. Carson — J. E. O'Brien
Harold Cole — E. W. Oesterreich
E. R. Coulbourn — J. S. Parsons
S. B. Crary — C. K. Poarch
P. J. Croft — L. M. Robertson
A. E. Curtis — F. E. Sanford
H. A. Dambly — E. V. Sayles
A. E. Davison — A. N. Shealy
E. W. Dillard — A. E. Silver
W. B. Fisk — F. V. Smith
I. W. Gross — K. M. Smith
J. H. Hagenguth — E. C. Starr
K. E. Hapgood — H. M. Trueblood
E. R. Hendrickson — E. R. Whitehead
J. B. Hodtum — R. L. Witzke

TRANSMISSION AND DISTRIBUTION SUBCOMMITTEES

1. Capacitor Subcommittee

F. V. Smith, *chairman* — E. R. Hendrickson
M. I. Altmansky — R. E. Marbury
G. Appleman — N. A. Miller
C. E. Baugh — R. B. Miller
E. W. Dillard — C. E. Parks
R. H. Earle — W. L. Steinbarge
K. E. Hapgood — L. T. Williams

Projects:

(*a*). Revision of Standard *C55* (1934) of capacitors.

(*b*). Report on operating practice—fusing practice, circuit characteristics, grounding of neutral, telephone switching, and so forth, in an operating guide.

E. R. Hendrickson, *chairman*

(*c*). Study of troubles with capacitors.

C. E. Parks, *chairman*

2. Distribution Subcommittee

Harold Cole, *chairman* — D. W. Nethercut
D. K. Blake — E. W. Oesterreich

(*Continued on page 1790*)

T. J. Brosnan — J. S. Parsons
E. R. Hendrickson — R. R. Robinson
J. B. Hodtum — F. E. Sanford

Projects:

(a). Joint working group with transmission systems.
T. J. Brosnan — E. R. Hendrickson — J. S. Parsons

(b). Working group on primary systems.
D. K. Blake — L. M. Robertson — F. E. Sanford

(c). Distribution transformers to the customer.

3. General Systems Subcommittee

S. B. Crary, *chairman* — A. A. Kronebery
O. A. Demuth — E. L. McClure
L. B. LeVesconte — L. M. Robertson
R. G. Hooke — H. M. Trueblood
E. K. Huntington — R. L. Witzke

Projects:

(a). Subject committee on line outages (joint with EEI).
R. W. Caswell — M. H. Pratt (*for EEI*)
G. D. Floyd — H. M. Trueblood
K. W. Hapgood — R. L. Witzke

(b). Report on power system overvoltages.

(c). Frequency changeover.

(d). Contamination and life of insulators.

4. Lightning and Insulator Subcommittee

J. T. Lusignan, *chairman* — E. L. Harder
H. N. Ekvall — W. W. Lewis
H. A. Frey — G. D. McMann
I. W. Gross — S. K. Waldorf
E. R. Whitehead

Projects:

(a). Recommendations for lightningproof lines.
E. R. Whitehead, *chairman*

(b). Impulse characteristics of insulators, wood, and air in series.

(c). Insulator contamination and corrective measures.

(d). Insulator flashover characteristics.

5. Towers, Poles, and Conductors Subcommittee

A. E. Davison, *chairman* — E. L. Kanouse
N. K. Barr — W. I. Middleton
C. A. Booker — A. A. Osipovich
P. J. Croft — J. A. Rawls
A. L. Duna — A. S. Runciman
E. Hansson — E. J. Staubitz
G. W. Stickley

Industry Group

Industry Co-ordinating Committee

J. J. Orr, *chairman*, United States Rubber Company, 1230 Sixth Avenue, New York 20, N. Y.

T. R. Rhea
Victor Siegfried
Ex-officio
Chairmen of the seven following technical committees in the industry group

Chemical, Electrochemical, and Electrothermal Applications

F. R. Benedict, *chairman*, 930 Milton Avenue, Pittsburgh, Pa.
F. V. Andreae — R. J. Kuhn
F. T. Bowditch — E. H. Lamberger
J. D. Brance — D. H. Levy
W. H. Dickinson — J. A. McLellan
W. C. Dreyer — G. H. Orcutt
M. E. Gamble — Waldo Porter
Felix Glaza — R. L. Rayner
L. M. Goldsmith — T. R. Rhea
L. O. Grondahl — H. C. Riggs
W. E. Gutzwiller — L. W. Roush
E. A. Hanff — G. B. Scheer
C. W. Hansell — E. H. Scheick
J. D. Harper — E. O. Scott
T. G. Hieronymus — H. P. Sherer
J. E. Hobson — I. R. Smith
H. E. Houck — Joel Tompkins
J. D. Huntzberger — J. J. Unger
M. A. Hyde, Jr. — G. W. Vinal
Rollin Kennard — H. W. Wahlquist
E. L. Kirk — H. H. Zielinski

CHEMICAL, ELECTROCHEMICAL, AND ELECTROTHERMAL SUBCOMMITTEES

1. Arc Furnaces and Electrothermal Processes Subcommittee

E. H. Browning, Jr., *chairman* — E. A. Hanff
F. V. Andreae — A. R. Olirogge

(*Continued in next column*)

F. R. Benedict — E. Scheick
W. L. Bundy — H. H. Zielinski

2. Cathodic Protection Subcommittee

Scope: To develop the electrical engineering aspects of the application of cathodic protection. To promote a general understanding of those factors, under the control of designing and operating engineers, that contribute to corrosion and to its mitigation. To prepare a technical paper on the subject of electrical design from the standpoint of reduction of corrosion.

E. L. Kirk, *chairman* — I. A. Dennison
A. E. Archambault — L. J. Gorman
J. D. Brance — T. G. Hieronymus
A. B. Brooks — R. J. Kuhn
Guy Corfield — D. H. Levy
C. G. Cox — R. L. Rayner
H. W. Wahlquist

3. Electrolytic Processes Subcommittee

Scope: Treatment of all matters relative to electrolytic processes such as electrodeposition, electrolytic refining, electrolytic reduction, alkali chloride electrolysis, aqueous electrolytic processes. Such subjects as theory, technique, efficiency, plant, electrolytic cells, application of power conversion equipment, specialized equipment, and operating hazards are within the scope of this subcommittee.

W. E. Gutzwiller, *chairman* — Rollin Kennard
Felix Glaza — F. L. Lawton
J. D. Harper — G. H. Orcutt
H. E. Houck — Waldo Porter
F. L. Kaestle — G. B. Scheer
J. Tompkins

4. Petroleum Refining and Production Subcommittee

Lester Goldsmith, *chairman* — E. L. Hoyle
H. J. Appel — D. H. Levy
W. H. Dickinson — E. L. Nopper
J. B. Glasby — L. E. Priester

5. Storage Batteries Subcommittee

Scope: This subcommittee has as its function the fostering of technical papers and the establishment of AIEE Standards, where needed, on all phases of storage battery design and engineering.

H. C. Riggs, *chairman* — E. T. Rummel
J. D. Huntzberger — J. J. Unger
L. A. Murray — K. A. Vaughn
H. H. Zielinski

6. Chemical Industries Subcommittee

L. W. Roush, *chairman* — Felix Glaza — H. E. Houck

Electric Heating

G. W. Scott, Jr., *chairman*, Armstrong Cork Company, Lancaster, Pa.
E. L. Bailey — F. H. Mason
R. M. Baker — W. H. Meyer
E. H. Browning — L. R. Milburn
Harold Bunte — R. Neighbours
F. T. Chesnut — A. E. Opdenweyer
L. M. Duryee — C. E. Peck
R. H. Ellis — K. Pinder
P. H. Goodell — J. B. Rice
J. Grotsinger — W. C. Rudd
J. C. Handy — E. H. Scheick
E. A. Hanff — W. R. Schofield
J. B. Hodtum — G. S. Smith
L. P. Hynes — T. H. Story
T. P. Kinn — J. T. Thwaites
G. F. Leland — G. E. Walters
H. R. Winemiller

ELECTRIC HEATING SUBCOMMITTEES

1. Electronic Heating Subcommittee

Scope: Study of the problem of interference from electronic heating equipment (with the advance of the Electronic Heating Subcommittee, the Federal Communications Commission has formulated regulations regarding the allowable limit of radiation from electronic heating equipment). Formulation of the principles of good engineering practice to deal with intolerable interference. Development of an AIEE Standard for electronic heating equipment including definitions of electronic heating terms.

W. C. Rudd, *chairman* — T. R. Kennedy
W. R. G. Baker — T. P. Kinn
G. P. Bosomworth — W. H. Meyer
L. W. Chapin — Loyal R. Milburn
F. Chesnut — Eugene Mittelman
Harold E. Dinger — G. W. Scott, Jr.
L. M. Duryee — J. T. Thwaites
J. E. Eiselein — G. E. Walters
C. W. Frick — D. E. Watts
H. C. Gillespie — Otto Weitmann

(*Continued in next column*)

W. H. Hickok — H. R. Winemiller
R. J. Hunn — R. L. Uphoff — A. C. Wooldridge

2. Radiant Heating Subcommittee

Scope: Standardization of lamp-type heating in all its phases. The advantages and limitations of infrared heating will be emphasized for the benefit of the engineering profession. It will cover the construction, optics, thermodynamics, and safety considerations associated with all high temperature sources, correlation with other forms of electric heating, but not to include space heating.

Paul H. Goodell, *chairman* — S. G. Hibben
C. E. Russell, *secretary* — J. E. Johanson
L. J. Barber — E. A. Linaday
E. J. Bates — A. D. Moore
Earl Benson — C. T. Prendergast
P. O. Blackmore — G. W. Scott, Jr.
A. H. Canada — Lawrence Stout
Charles A. Casey — J. E. Sump
H. J. Garber — Ernest Upton
E. D. Haigler — W. H. Wagner

3. Subcommittee on Radiation Measurements Above 200 Megacycles

Scope: All methods of measuring radiation up to and above 200 megacycles are being studied. Actual experimental work in various laboratories is being undertaken to develop methods of measurement in the frequency ranges where a satisfactory method does not now exist and to improve methods in those ranges where some work has been done to date.

R. M. Baker, *chairman* — H. E. Dinger
H. H. Beitzer — J. E. Eiselein
Marvin Bock — C. W. Frick
R. E. Burgess — G. W. Scott, Jr.
E. W. Chapin — Ralph M. Showers
Lucien Clarke — A. J. Vadasz — H. E. Sorrows

4. Resistance Heating and Electric Furnaces Subcommittee

Scope: To study and promulgate information on the use of resistance heating and electric furnaces in industry including such standardization as may be necessary.

L. P. Hynes, *chairman* — A. G. Hotchkiss
F. E. Bash — C. E. Peck
Mark M. Greer — K. Pinder
J. C. Handy — David Y. Robinson
E. H. Scheick

Electric Welding

E. H. Vedder, *chairman*, Westinghouse Electric Corporation, P. O. Box 2025, Buffalo 5, N. Y.
E. M. Callender — J. R. Sellenberg
G. M. Chute — Sam Shiozawa
C. N. Clark — C. E. Smith
W. E. Crawford — H. W. Snyder
J. F. Deffenbaugh — H. I. Stanback
R. C. Freeman — E. F. Steinert
G. W. Garman — G. I. F. Theriault
J. C. Learman — H. W. Tietze
W. M. McKie — F. J. Wells
R. C. McMaster — B. L. Wise
C. M. Rhoades, Jr. — R. E. Young
F. Rothganger — Myron Zucker

ELECTRIC WELDING SUBCOMMITTEES

1. Power Supply for Resistance Welding Machines Subcommittee

Scope: Provision of basic information relating to power requirements and the effect of resistance welding load from the standpoint of both users and utilities. Outline economic considerations in the purchase and utilization, as well as provision of power, for welding including power factor correction, combined power and welding facilities, and so forth. Set up desirable practices and methods of procedure to be used in the selection of adequate power facilities for resistance welding.

C. M. Rhoades, Jr., *chairman* — R. S. Phair
W. H. Allen — C. E. Smith
W. G. Bostwick — J. L. Solomon
W. W. Brumby — C. B. Stadum
C. N. Clark — H. W. Tietze
E. F. Dissmeyer — Myron Zucker

2. Resistance Welding Subcommittee

C. E. Smith, *chairman* — C. N. Clark
E. M. Callender — Myron Zucker — G. W. Garman

3. Arc Welding Subcommittee

E. F. Steinert, *chairman* — R. R. LoBosco
C. N. Clark — G. I. MacGuffie
E. L. Hansen — H. W. Snyder

General Industry Applications

L. A. Umansky, *chairman*, General Electric Company, industrial engineering divisions, 1 River Road, Schenectady 5, N. Y.
B. T. Anderson — A. F. Lukens

(*Continued on page 1791*)

A. T. Bacheler — Joseph Mannele
S. A. Bobe — E. F. Mekelburg
D. C. Christison — J. F. Moore
J. M. Delfs — E. K. Murphy
J. C. Fink — D. R. Percival
A. W. Frankenfield — Kennard Pinder
J. A. Gienger — H. A. Rose
F. B. Haeussler — H. L. Smith
E. M. Hays — R. H. Smith
L. W. Herchenroeder — F. D. Snyder
K. W. John — R. F. Snyder
V. O. Johnson — Ray Stroppel
G. W. Knapp — W. B. Wigton

GENERAL INDUSTRY APPLICATIONS SUBCOMMITTEES

1. Machine Tools Subcommittee

W. B. Wigton, *chairman* — J. M. Dickenson
B. T. Anderson — L. W. Herchenroeder
E. L. Bailey — E. F. Mekelburg
J. M. Delfs — R. E. Stroppel — D. R. Percival

2. Materials Handling Subcommittee

E. M. Hays, *chairman* — D. C. Gray
E. L. Blankenbeker, *secretary* — R. W. Mallick
Fred W. Atz — A. H. Myles
O. M. Bundy — E. J. Posselt
M. A. de Ferranti — E. C. Rice
E. O. Dunham — C. B. Rissler
T. O. English — G. W. Yanney

3. Pulp and Paper Industry Subcommittee

G. W. Knapp, *chairman* — Floyd L. Davis
V. B. Baker — Oscar O. Fisk
H. B. Barrow — E. K. Murphy
A. C. Bird — H. Ostertag
D. C. Christison — Ray Smith — H. A. Rose

4. Rubber and Plastics Industries Subcommittee

K. W. John, *chairman* — V. O. Johnson
A. T. Bacheler — G. V. Kullgren
P. S. Bechtol — B. D. Morgan
B. J. Dalton — W. J. Secrest
J. A. Gienger — R. F. Snyder — H. L. Smith

5. Textile Industry Subcommittee

F. D. Snyder, *chairman* — L. T. Jester
S. A. Bobe — A. P. Lewis
R. J. Derviartini — D. McConnell
A. W. Frankenfield — Victor Sepavich
H. Haley — R. S. Stribling
F. B. Haeussler — H. C. Uhl

Industrial Control

J. D. Leitch, *chairman*, Electric Controller and Manufacturing Company, 2700 East 79th Street, Cleveland 4, Ohio

E. H. Alexander — E. F. Mekelburg
H. H. Angel — M. Michel
F. J. Bartholomew — T. B. Montgomery
L. J. Conover — E. E. Moyer
J. A. Cortelli — L. B. Murray
R. H. Ellis — H. L. Palmer
C. T. Evans — J. W. Picking
L. E. Fisher — L. T. Rader
D. K. Frost — G. H. Rendel
J. E. Gahagan — E. J. Rivoira
J. A. Gienger — J. B. Russell
G. W. Heumann — W. Schaichlin
K. W. John — R. E. Schuette
R. W. Jones — A. G. Turnbull
E. U. Lassen — E. H. Vedder
L. H. Mathias — H. R. Walker

INDUSTRIAL CONTROL SUBCOMMITTEES

1. Bibliography Subcommittee

R. W. Jones, *chairman*

2. Electronic Control Subcommittee

Scope: The treatment of all matters in which the dominant factors are the design, construction, and functioning of electronic industrial controls exclusive of those controls and functions that specifically are assigned to other technical committees.

H. L. Palmer, *chairman* — F. G. Logan
F. T. Bailey — E. B. McDowell
H. B. Black — J. W. Picking
S. L. Burgwin — V. J. Porter
C. J. Collom — K. P. Puchlowski
Ben Cooper — W. G. Roman
E. T. Davis — C. A. Schurr
W. H. Elliot — W. W. Snyder
William Few — H. I. Stanback
E. W. Hutton — B. B. Stuart
L. U. C. Kelling — C. M. Summers
O. W. Livingston — B. F. Tellkamp

Working Groups:

(2-1). Electronic Control of D-C Motors

J. W. Picking, *chairman* — William Few
H. B. Black — F. G. Logan
C. J. Collom — W. G. Roman
Ben Cooper — H. I. Stanback
W. H. Elliot — B. B. Stuart
B. F. Tellkamp

(2-2). Photoelectric Pickup Systems

L. U. C. Kelling, *chairman* — F. T. Bailey

(2-3). Electronic Relaying Devices

E. B. McDowell, *chairman* — C. A. Schurr
K. P. Puchlowski — B. F. Tellkamp

(2-4). Electronic Regulators and Regulating Systems

O. W. Livingston, *chairman* — W. H. Elliot
H. B. Black — William Few
S. L. Burgwin — F. G. Logan
E. T. Davis — J. W. Picking
W. W. Snyder

(2-5). Electronic Control of A-C Power

E. W. Hutton, *chairman* — F. G. Logan
H. B. Black — W. G. Roman
W. H. Elliot — B. F. Tellkamp

3. Standards Subcommittee

Scope: This subcommittee will prepare proposals for revised and new Standards pertaining to industrial control in which AIEE is interested, especially AIEE Standard 15 which is also ASA Standard C19.1. Such proposals are in preparation to action taken by official AIEE members on the respective ASA committees. The work of co-ordinating is carried on by the subcommittee chairman who assembles working groups from time to time as the need for action on existing or new Standards arises.

G. W. Heumann, *chairman*

4. Test Codes Subcommittee

Scope: The proposed test code should contain recommendations for test procedure and test methods, as applied to industrial control equipment. The test methods recommended should be such that the tests made at different times and by different laboratories will be comparable and significant. It is desirable that the test procedures be such that the results of the tests may be interpreted and expressed in some defined scale of comparison, but it is not the purpose of the test code to set standards of acceptability. Neither should the test code attempt to determine or specify what type of investigation need be made on any given type of apparatus. It is the consensus of opinion that the efforts of the subcommittee should be concentrated on the more fundamental tests relating to basic design rather than on routine production tests.

C. T. Evans, *chairman* — H. E. Nason
D. K. Frost — W. E. Pakala
B. W. Jones — H. E. Ryan
C. A. Schaefer

Industrial Power Systems

D. L. Beeman, *chairman*, General Electric Company, industrial engineering divisions, Schenectady 5, N. Y.

W. M. Ballenger — J. S. Morgan
H. G. Barnett — W. A. Mosteller
W. C. Bloomquist — J. S. Parsons
E. S. Dibble — R. C. Peters
Felix Glass — C. G. Pionnasen
J. S. Gault — Dick Ray
W. N. Goodridge — W. H. Rollman
W. S. Gordon, Jr. — F. Rothganger
D. C. Gray — R. C. Settersrom
L. N. Grier — R. R. Shollenberger
W. R. Harmer — J. F. Spease
C. F. Hedlund — H. C. Swannell
C. R. Johnson — T. O. Sweatt
R. E. Johnson — B. F. Thomas
G. M. Lathrop — S. A. Warner
M. C. Mapes — R. W. Watson
A. E. Marshall — William Zelley
J. L. McKeever — T. O. Zittel

INDUSTRIAL POWER SYSTEMS SUBCOMMITTEES

1. Subcommittee on Interior Wiring Design for Commercial Buildings

B. F. Thomas, Jr., *chairman* — Ralph C. Hodges
L. W. McCullough, *secretary* — R. H. Kaufmann
F. R. Benedict — George K. Perley
C. D. Bratiotis — L. C. Peterman
Leo H. Cleary — H. F. Richardson
Allan Coggeshall — C. W. Schemm
W. T. Crosman — C. F. Scott
R. F. Davis — B. Z. Segall
V. E. Dodson — Herbert Speight
C. F. Hedlund — Howard R. Stevenson
James A. Heffernan — T. O. Sweatt
W. Weinerth

2. Subcommittee on Revision of Electric Power Distribution for Industrial Plants

John Grotzinger, *chairman* — L. E. Fisher

(Continued in next column)

J. A. Arberry — C. R. Johnson
G. E. Baker — N. B. Lainge
H. G. Barnett — S. W. Luther
W. C. Bloomquist — R. B. McKinley
E. T. Carlson — J. S. Parsons
R. F. Davis — R. C. R. Schulze
Wm. Deans — E. E. Turkington
D. D. Douglass — R. J. Wiseman

3. Transformer Voltage Ratios Subcommittee

H. G. Barnett, *chairman* — J. Grotzinger
D. L. Beeman — K. Pinder
S. F. French — T. O. Sweatt
C. C. Whipple

Mining and Metal Industry

F. W. Cramer, *chairman*, Carnegie-Illinois Steel Corporation, Carnegie Building, Fifth Avenue, Pittsburgh 19, Pa.

G. Bright — R. C. McMaster
M. K. Clay — W. E. Miller
R. J. Corfield — T. B. Montgomery
J. A. Cortelli — A. C. Muir
E. W. Davis — H. P. Musser
J. M. Downie — Bailey E. Price
J. H. Edwards — D. E. Renshaw
R. H. Ellis — E. G. Schlup
J. R. Erbe — F. O. Schnure
Cecil Farrow — Victor Siegfried
James J. Fitzgibbon — O. J. Swanson
Lanier Greer — C. L. Tyler
L. N. Grier — T. R. Weichel
W. R. Harris — A. J. Whitcomb
L. H. James — C. O. Wood
C. F. King, Jr. — I. O. Zittel

MINING AND METAL INDUSTRY SUBCOMMITTEES

1. Metal Industries Subcommittee

T. B. Montgomery, *chairman* — C. F. King
J. A. Cortelli — W. E. Miller
R. H. Ellis — Bailey Price
J. R. Erbe — E. G. Schlup
Cecil Farrow — F. O. Schnure
L. N. Grier — Victor Siegfried
W. R. Harris — A. J. Whitcomb

2. Mining Subcommittee

A. C. Muir, *chairman* — James J. Fitzgibbon
Graham Bright — R. C. McMaster
M. K. Clay — H. P. Musser
R. J. Corfield — D. E. Renshaw
E. W. Davis — O. J. Swanson
John M. Downie — C. L. Tyler
J. H. Edwards — T. R. Weichel
L. H. James — C. O. Wood

Communication and Science Group

Communication and Science Co-ordinating Committee

J. D. Tebo, *chairman*, Bell Telephone Laboratories, 463 West St., New York 14, N. Y.

J. L. Callahan — I. Melville Stein

Ex-officio

Chairmen of the eight following technical committees in the communication and science group

Basic Sciences

Walther Richter, *chairman*, Allis-Chalmers Manufacturing Company, West Allis, Wis.

J. A. Becker — W. A. Lewis
J. G. Brainerd — M. G. Malti
C. R. Burrows — W. H. MacWilliams, Jr.
F. M. Clark — G. D. McCann
C. Concordia — K. W. Miller
P. C. Cromwell — W. E. Phillips
A. V. Eastman — H. J. Plumley
E. A. Gaugler — J. B. Russell
W. C. Hall — W. J. Seeley
C. R. Hanna — J. J. Smith
C. W. Hansell — L. A. Smith
F. K. Harris — V. G. Smith
H. L. Hazen — F. G. Tappan
N. S. Hibshman — J. D. Tebo
T. J. Higgins — F. E. Terman
W. C. Johnson — G. S. Timoshenko
T. D. Yensen

BASIC SCIENCES SUBCOMMITTEES

1. Applied Mathematics Subcommittee

Scope: To furnish needed co-operation between mathematicians and engineers, preferably by bringing specific engineering problems to the attention of mathematicians for solution rather than to consider only general problems which might be discussed from an abstract viewpoint. Members of the American Mathematical Society

are invited to join with this subcommittee in mutually advantageous conferences and sessions.

M. G. Malti, *chairman* — Henry B. Hanateen
P. L. Alger — J. J. Smith
Harry Sohon

2. Electric Circuit Theory Subcommittee

Scope: Considerable work in circuit theory has been done, particularly as a result of the war effort. The function of this subcommittee is to make a thorough review of recent circuit theory developments in all fields directed toward the publication of this material and also to study circuit theory definitions which are needed urgently in view of the almost complete lack of standard circuit theory definitions existing at the present time.

T. J. Higgins, *chairman* — F. J. Maginniss
J. G. Brainerd — W. O. Osbon
R. L. Dietzold — E. B. Payne
E. A. Guillemin — W. E. Phillips
E. Weber

3. Energy Sources Subcommittee

Scope: The great impetus given to new methods of supplying electric power by developments in nuclear energy has suggested that all known methods of producing electric power should be reviewed in the light of present-day knowledge. This subcommittee is conducting conferences to review the various known means of electric energy generation, efforts being made to prepare for publication papers by outstanding experts in their respective fields.

W. A. Lewis, *chairman* — L. W. Matsch
W. C. Brown — Walther Richter
M. A. Gartens — J. J. Smith
C. R. Hanna — B. R. Teare
J. D. Tebo

4. Electrical Properties of Solids Subcommittee

Scope: Treatment of the theories explaining the electrical properties of various types of solid materials commonly used in electrical engineering, such as thermionic emission, semiconductors, dielectrics, and magnetism.

Joseph A. Becker, *chairman* — K. Lark-Horovitz
Stephan Angello — P. H. Miller, Jr.
J. P. Barton — Fred Seitz
Saul Dushman — Lloyd Smith
R. W. Engstrom — A. Von Hippel
F. K. Harris — Dean W. Wooldridge

5. Subcommittee on Magnetics

T. D. Yensen, *chairman* — E. A. Gaugler
J. P. Barton — S. L. Quimby
R. M. Bozorth — W. E. Ruder
G. H. Cole — Van Vleck
H. G. Zambell

6. Subcommittee on Electrical Properties of Liquids and Gases

(*Personnel to be selected*)

Communication

Rothwell E. Smith, *chairman*, International Telephone and Telegraph Corp., 67 Broad St., New York 4, N. Y.

L. G. Abraham — P. L. Johnson
H. A. Affel — E. W. Kenefake
A. L. Albert — R. H. Kline
W. M. Allen — H. P. Lawther
G. M. K. Baker — M. Levy
L. D. Becken — G. A. Locke
J. D. Booth — R. E. Mathes
F. B. Bramhall — G. R. Messmer
A. J. Busch — J. L. Middlebrooks
I. F. Byrnes — F. E. Norris
J. L. Callahan — R. R. O'Connor
J. B. Coleman — L. G. Pacent
L. G. Cumming — E. G. Ports
C. E. Dean — R. H. Ranger
M. Eastham — G. B. Ransom
H. E. Ellithorn — W. A. Ready
A. E. Frost — F. M. Rives
J. C. Gaudio — H. I. Romnes
D. G. Geiger — G. T. Royden
P. C. Goldmark — A. L. Rumsey
T. T. Goldsmith, Jr. — J. B. Russell
G. H. Gray — R. E. Shelby
L. B. Grew — A. B. Smith
H. H. Haglund — J. E. Smith
W. D. Hailes — W. T. Smith
L. C. Holmes — S. C. Spielman
E. M. Johnson — R. B. Vaile, Jr.
W. E. Vellines, Jr.

COMMUNICATION SUBCOMMITTEES

1. Co-ordinating Committee

Scope: To act as liaison between the technical program committee and the working units of the communication committee.

R. E. Smith, *chairman* — F. B. Bramhall, *secretary*
J. L. Callahan, *junior past chairman*
H. I. Romnes, *vice-chairman*

Subcommittee chairmen:

H. A. Affel — H. H. Haglund
J. D. Booth — F. G. Ports
A. J. Busch — F. M. Rives
G. H. Gray — R. E. Shelby
S. C. Spielman

2. Communications Standards Subcommittee

Scope: To co-ordinate with other working units on standards in so far as it may concern communications.

G. H. Gray, *chairman*

3. Communication Switching Systems Subcommittee

Scope: Embodies telephone switching systems such as crossbar, panel, rotary and step-by-step and includes all forms of telegraph switching systems such as employed in handling switched telegraph message traffic.

A. J. Busch, *chairman* — P. L. Johnson
A. E. Frost — H. P. Lawther
D. G. Geiger — F. E. Norris
G. H. Gray — Arthur Bessey Smith
L. C. Holmes — D. L. Solomon

4. Wire Communication Systems Subcommittee

Scope: Covers wire transmission systems and associated apparatus as applied to open wire lines, land and submarine cables for providing telephone, telegraph, broadcast, and television program services.

H. A. Affel, *chairman* — G. H. Gray
L. G. Abraham — H. H. Haglund
Arthur L. Albert — R. R. O'Connor
W. M. Allen — G. B. Ransom
H. E. Ellithorn — A. L. Rumsey
Arthur Bessey Smith

5. Home Radio Receivers Subcommittee

Scope: This subcommittee comprises activities related to amplitude modulation, frequency modulation, and television receivers used for the reception from radio-broadcasting stations.

S. C. Spielman, *chairman* — P. C. Goldmark
J. B. Coleman — T. T. Goldsmith, Jr.
L. G. Cumming — E. W. Kenefake
C. E. Dean — M. Levy
J. C. Gaudio — H. W. Schaefer

6. Power Line Carrier Systems Subcommittee

Scope: This subcommittee would join with the power group in handling all activities related to the use of carrier currents by power companies in their operations.

F. M. Rives, *chairman* — W. D. Hailes
J. D. Booth — G. R. Messmer
W. T. Smith

7. Radiobroadcasting Systems Subcommittee

Scope: This subcommittee would cover amplitude modulation, frequency modulation, and television broadcasting stations, studio equipment, television cameras, studio-transmitter links, and anything else related to the broadcasting of programs for entertainment or instruction.

R. E. Shelby, *chairman* — T. T. Goldsmith, Jr.
G. M. K. Baker — E. M. Johnson
J. B. Coleman — E. W. Kenefake
P. C. Goldmark — J. L. Middlebrooks
J. Ernest Smith

8. Radio Communication Systems Subcommittee

Scope: This subcommittee would cover radio communication systems of all kinds such as radio relay for telephone, television or telegraph; mobile systems for automobiles, ships, or airplanes; and point-to-point systems for telephone or telegraph.

E. G. Ports, *chairman* — T. T. Goldsmith, Jr.
G. T. Royden, *vice-chairman* — E. W. Kenefake
E. D. Becken — G. R. Messmer
E. M. Boone — R. H. Ranger
I. F. Byrnes — John B. Russell
J. Ernest Smith

9. Record Communications Subcommittee

Scope: This subcommittee would handle facsimile telephoto, magnetic recording, teletypewriters, and other similar communications involving the making of a record.

H. H. Haglund, *chairman* — G. A. Locke
E. D. Becken — R. E. Mathes
J. O. Carr — R. H. Ranger
G. H. Gray — Arthur Bessey Smith
L. C. Holmes — R. B. Vaile, Jr.

10. Special Communication Applications Subcommittee

Scope: This subcommittee would handle miscellaneous activities which do not fall within the scope of the other committees such as hearing aids, radar, and sonar.

J. D. Booth, *chairman* — W. D. Hailes
R. H. Kline, *vice-chairman* — E. W. Kenefake
I. F. Byrnes — L. G. Pacent
J. B. Coleman — W. A. Ready
Melville Eastham — R. B. Vaile, Jr.

Computing Devices

Charles Concordia, *chairman*, central station engineering divisions, General Electric Company, 1 River Road, Schenectady 5, N. Y.

J. G. Brainerd — W. H. MacWilliams, Jr.
S. H. Caldwell — J. W. Mauchly
F. I. Harder — G. D. McCann
W. C. Johnson — J. C. McPherson

COMPUTING DEVICES SUBCOMMITTEES

1. Digital Computers Subcommittee

Doctor John W. Mauchly, *chairman*

2. Continuous-Variable Computers Subcommittee

Doctor E. L. Harder, *chairman*

Electronic Power Converters

H. Winograd, *chairman*, Allis-Chalmers Manufacturing Company, electrical department, Milwaukee 1, Wis.

H. R. Blomquist — D. E. Marshall
W. F. Bonner — A. J. Maslin
J. L. Boyer — L. W. Morton
J. H. Cox — R. A. Nelson
F. W. Cramer — M. E. Reagan
J. B. Donnelly — W. J. L. Rupprecht
Wm. Fraser — A. Schmidt, Jr.
W. E. Gutzwiller — W. C. Sealey
P. L. Hartsock — S. L. Smith
C. C. Herskind — H. C. Steiner
H. E. Houck — O. J. Swanson
G. N. Hughes — T. A. Taylor
Otto Jensen — J. T. Thwaites
C. R. Marcum — Joel Tompkins
C. H. Willis

ELECTRONIC POWER CONVERTERS SUBCOMMITTEES

1. Policy and Planning Subcommittee

Scope: The function of this subcommittee is to plan and co-ordinate the activities of the main committee and its subcommittees. It will consist of the officers and the preceding chairman of the main committee, the chairmen of the subcommittees, and any other committee members designated by the chairman.

Main Committee Officers:
H. Winograd, *chairman*
C. C. Herskind, *vice-chairman*
H. C. Steiner, *secretary*
C. H. Willis, *preceding chairman*

Subcommittee Chairmen:
C. R. Marcum — A. J. Maslin
D. E. Marshall — L. W. Morton
Joel Tompkins

2. Electronic Converter Application Subcommittee

Scope: This subcommittee will handle matters relating to the application and operation of electronic power converters, such as rating, installation, operation, maintenance, inductive co-ordination.

L. W. Morton, *chairman* — W. E. Gutzwiller
Joel Tompkins, *co-chairman* — H. E. Houck
F. W. Cramer — W. J. L. Rupprecht
J. B. Donnelly — S. L. Smith
Wm. Fraser — O. J. Swanson
N. W. Geist — T. A. Taylor
J. T. Thwaites

3. Electronic Converter Circuits Subcommittee

Scope: This subcommittee will handle matters relating to circuit theory, protection, and control of electronic power converters. It will co-operate with the other subcommittees and with other AIEE committees (such as electronics, switchgear, protective devices substations) on matters of mutual interest.

C. C. Herskind, *chairman* — G. F. Jones
W. F. Bonner — R. A. Nelson
J. L. Boyer — M. E. Reagan
E. V. De Blieux — A. Schmidt, Jr.
I. K. Dortort — T. A. Taylor
G. N. Hughes — C. H. Willis
H. Winograd

4. Metallic Rectifiers Subcommittee

Scope: This subcommittee so far has completed a set of standard definitions, a bibliography, and a patent list on metallic rectifiers. It is working now on a test code and also is collaborating with the United States Army and Navy in connection with the issuance of a Joint Army-Navy specification on metallic rectifiers. This subcommittee has future plans to issue standards for metallic rectifiers, as well as delve into the theoretical aspects of these rectifiers.

L. O. Grondahl, *chairman* — C. W. Hansell

(*Continued on page 1793*)

E. A. Harty, *secretary* — C. C. Herskind
W. F. Bonner — W. E. Phillips
L. W. Burton — N. Y. Priesman
P. G. Cobb — C. G. Ramsey
M. E. Gamble — I. R. Smith
K. S. Geiges — D. Trucksess
E. A. Groeteke — J. Yarmack
C. E. Hamann — S. Zwerling

5. Papers and Speakers Subcommittee

Scope: This subcommittee will complete the bibliography on electronic power converters and will keep it up to date with annual supplements. It will make recommendations on subjects for technical papers and promote the preparation of such papers. It will prepare information on the availability of speakers and educational material on electronic power converters, as an aid to schools, AIEE Sections, and discussion groups.

C. R. Marcum, *chairman* — J. T. Thwaites
J. B. Rice — H. E. Zuvers

6. Rectifier Transformers Subcommittee

Scope: This subcommittee will handle matters relating to transformer equipment used for electronic power converters. It will maintain liaison with the AIEE transformer committee, to keep the two committees informed on matters of mutual interest.

A. J. Maslin, *chairman* — E. V. DeBlieux
W. C. Sealey

7. Rectifying Devices Subcommittee

Scope: This subcommittee will handle matters relating to design and characteristics of rectifying devices used for electronic power converters. It will maintain liaison and co-operate with other technical committees of the AIEE and other organizations which carry on activities relating to such rectifying devices, and it will keep the electronic power converter committee informed on such activities.

D. E. Marshall, *chairman*

8. West-Coast Subcommittee

J. H. Cox, *chairman*

9. Other Projects from Last Year

(*a*). Preparation of report on protection of electronic power converters. By electronic converter circuits subcommittee; C. C. Herskind, chairman of working group.

(*b*). Bibliography on electronic power converters. By papers and speakers subcommittee; C. R. Marcum, chairman of working group.

(*c*). 1. Prepare forms for collecting application, operating, and maintenance data on mercury-arc power converters.

2. Collect data on a-c system characteristics and load characteristics in rectifier installations for various industries.

3. Power metering in rectifier installations. By electronic converter application subcommittee; L. W. Morton, chairman.

(*d*). Prepare report on inductive co-ordination relating to d-c side of rectifier installations. By electronic converter application subcommittee; T. A. Taylor, chairman of working group.

Electronics

C. H. Willis, *chairman*, Princeton University, Princeton, N. J.

G. M. K. Baker — O. W. Livingston
W. R. G. Baker — D. E. Marshall
R. Bennett — R. C. Mason
E. M. Boone — F. T. McNamara
C. J. Breitwieser — H. R. Meahl
C. B. Brown — G. A. Morton
G. H. Browning — B. K. Northrop
C. V. Bullen — J. K. Nunan
R. S. Burnap — D. Packard
W. R. Clark — H. L. Palmer
L. G. Cowles — G. W. Penney
G. J. Crowdes — W. H. Pickering
H. F. Dart — O. W. Pike
E. V. DeBlieux — A. J. Quigley
W. G. Dow — J. G. Reid, Jr.
A. V. Eastman — L. E. Reukema
G. V. Elizroth — J. D. Ryder
R. Feldt — E. T. Sherwood
D. G. Fink — H. B. Slade
K. S. Geiges — S. W. Smith
W. A. Geohegan — T. Spooner
E. W. Greenfield — H. C. Steiner
E. R. Haberland — C. M. Summers
L. P. Haner — A. A. Sylvane
J. D. Heibel — E. R. Thomas
J. E. Hobson — J. T. Thwaites
J. R. Hyneman — F. N. Tompkins
T. P. Kinn — D. E. Trucksess
W. H. Lee — D. C. Ulrey
W. A. Leidigh — W. C. White
J. D. Leitch — H. Winograd
A. M. Zarem

ELECTRONICS SUBCOMMITTEES

1. Electronic Aids to Navigation Subcommittee

Scope: The formulation of definitions of radio and electronic navigational terms. This work is being co-ordinated with the radio technical commission for aeronautics. In future months it is expected that the committee will undertake to formulate standards of testing various radio and electrical navigation systems.
(Personnel not yet selected)

2. Electronic Precipitation Subcommittee

Scope:

1. Electrical precipitation of dust, both in air conditioning and in the removal of high concentrations of dust fume and smoke which generally has been called Cottrell precipitation.
2. Electrostatic painting and "detearing."
3. Electrostatic separation of solids.
4. Electrostatic application of abrasives and other substances in manufacturing processes.
5. Static problems in industry.

(G. W. Penney, *chairman*)

3. Electronic Papers, Meetings and Section Contacts Subcommittee

Scope: Encourages and initiates the preparation of papers on any phase of electronics.
W. C. White, *chairman*

4. High-Frequency Conductors, Cables, and Connectors Subcommittee

Scope: The subcommittee has under consideration possible standardization, procurement of technical papers, and liaison work with other technical sections in connection with high-frequency cables, conductors, and connectors. The scope of this subcommittee also will include wave guides. The subcommittee has been actively considering coaxial cables, other high-frequency wires and connectors, and test methods.
G. J. Crowdes, *chairman*

5. Hot-Cathode Electronic Power Converters Subcommittee

Scope: Preparation of standards that apply to the following types of converters: rectifiers utilizing hot cathode gas or vacuum tubes and inverters utilizing hot cathode gas tubes. This scope excludes applications in oscillator circuits and radio receivers.
D. E. Trucksess, *chairman*

6. Subcommittee for Liaison with IRE

R. S. Burnap, *chairman*

7. Subcommittee for Liaison with JETEC (Joint Electron Tube Engineering Council)

O. W. Pike, *chairman*

8. West Coast Subcommittee

W. H. Pickering, *chairman*

9. Canadian Liaison Subcommittee

J. E. Thwaites, *chairman*

10. Electronic Education Subcommittee

J. D. Ryder, *chairman*

11. Electronic Aids to Medicine Subcommittee

Scope: This subcommittee will attempt to co-ordinate medical and engineering development, acquainting the engineering profession with medical problems requiring engineering development, and acquainting the medical profession with possibilities in solution of their problems.
W. A. Geohegan, *chairman*

12. X-Ray Tubes and Apparatus Subcommittee

Scope: Matters pertaining to X-ray tubes, rectifier tubes used in X-ray high-voltage generators, and other electron tubes used in X-ray apparatus and accessories. It contemplates work on such projects as standardizing the method of specifying the ratings and characteristics of X-ray and rectifier tubes; standardizing the symbols and form of presenting the wiring diagrams for X-ray generators; and standardizing the method of specifying the characteristics and ratings of X-ray transformers and generators.
Scott W. Smith, *chairman*

13. Electron Tubes Subcommittee

Scope: Treatment of all technical matters relating to the design, construction, test, and operation of electron tubes for industrial use. The committee is starting the preparation of standards. These standards will include sections on high vacuum tubes, cathode-ray tubes, gas-filled thermionic and cold cathode tubes, phototubes, and mercury-pool cathode tubes. All pertinent information such as definitions, standards, test codes, and application practice codes that is necessary to define electron tubes technically is to be included.
H. C. Steiner, *chairman*

Working Groups:

(*13-1*). High Vacuum Tubes
(*13-2*) Cathode-Ray Tubes
(*13-3*). Gas-Filled Thermionic and Cold Cathode Tubes—Phototubes
(*13-4*). Mercury-Pool Cathode Tubes

14. Electronic Standards and Definitions Subcommittee

Scope: It is the function of this subcommittee to assist the various electronic subcommittees in co-ordinating their standardizing efforts and to get them into the proper form and channels, if they are to become AIEE or American Standards. A part of this subcommittee's function is educational, in that it is attempting to show how standardization is accomplished, what national societies and associations are concerned, and how their activities are correlated.
T. Spooner, *chairman*

15. Electronic Aids to Geophysics

L. G. Cowles, *chairman*

16. Electronic Preparation and Preservation of Food
(Personnel not yet selected)

17. Infrared Applications

A. H. Canada, *chairman*

18. Liaison with Nucleonics

G. A. Morton, *chairman*

Instruments and Measurements

E. I. Green, *chairman*, Bell Telephone Laboratories, Inc., 463 West Street, New York 14, N. Y.

R. C. Bailey — H. C. Koenig
P. A. Borden — Everett S. Lee
H. H. Brauer — B. E. Lenehan
D. L. Brown — J. T. Lusignan, Jr.
A. L. Brownlee — H. Lyons
L. A. Burckmyer, Jr. — H. C. Martin
S. H. Caldwell — L. W. Matsch
J. H. Chiles — C. E. Melville
W. R. Clark — J. H. Miller
J. E. Clem — J. A. Morris
A. B. Craig — J. G. Reid, Jr.
C. L. Dawes — R. D. Richardson
F. C. Doble — J. B. Russell
E. D. Doyle — C. F. Savage
C. K. Duff — J. P. Scott
G. W. Dunlap — F. B. Silsbee
W. N. Eddy — D. B. Sinclair
F. J. Gaffney — M. B. Stout
T. S. Gray — E. L. Tornquist
C. M. Hathaway — W. R. Turner
G. B. Hoadley — J. M. Vanderleck
E. B. Kipp — L. K. Yerger
W. G. Knickerbocker — D. A. Young
A. E. Knowlton — C. J. Zeller

INSTRUMENTS AND MEASUREMENTS SUBCOMMITTEES

1. Subcommittee on Definitions

C. L. Dawes, *chairman* — W. N. Goodwin, Jr.
J. E. Clem — Everett S. Lee
Arthur B. Craig — B. E. Lenehan
H. C. Dickinson — P. MacGahan
F. B. Silsbee

2. Subcommittee on Electrical Tests on Dielectrics in the Field

F. C. Doble, *chairman* — E. D. Doyle
D. L. Brown — W. N. Eddy
A. L. Brownlee — I. W. Gross
C. T. Burke — Everett S. Lee
E. B. Curdu — D. B. Sinclair
Chester L. Dawes — G. M. L. Sommerman
L. Wetherill

3. Subcommittee on High-Frequency Measurements

F. J. Gaffney, *chairman* — G. B. Hoadley
I. G. Easton — H. Lyons
E. P. Felch, Jr. — H. R. Meahl
F. Hamberger, Jr. — H. E. Webber
H. A. Wheeler

4. Subcommittee on Marking of Varmeters and Related Instruments

Arthur B. Craig, *chairman* — Walter G. Knickerbocker
A. L. Brownlee — A. E. Knowlton
A. L. Carvill — F. B. Silsbee
E. B. Kipp — H. B. Smith

5. Subcommittee on Master Test Code for Resistance Measurements

F. B. Silsbee, *chairman* — Everett S. Lee
E. D. Doyle — L. W. Matsch
D. B. Sinclair

6. Subcommittee on Master Test Code for Temperature Measurements

L. A. Burckmyer, Jr., *chairman* — A. K. Jocks
F. A. Borden — R. D. Richardson
E. D. Doyle — M. D. Ross
W. R. Hough — S. S. Stack

7. Subcommittee on Revision of AIEE Standard 4 (Measurement of Test Voltage in Dielectric Tests)

J. T. Lusignan, *chairman* — J. H. Hagenguth
P. L. Bellaschi — W. B. Kouwenhoven

(*Continued on page 1794*)

A. L. Brownlee — R. S. Lapp
J. S. Carroll — W. W. Lewis
J. J. Clark — M. Newman
J. E. Clem — W. C. Sealey
E. W. Davis — F. B. Silsbee
C. M. Foust — W. T. Smith
H. A. Frey — R. W. Sorensen
I. W. Gross — W. L. Teague
S. W. Zimmerman

8. Subcommittee on Revision of C 39 (Electric Indicating Instruments)

H. C. Koenig, *chairman* — C. F. Savage
R. D. Hickok, Sr. — F. B. Silsbee
E. S. Lee — J. M. Vanderleck
P. MacGahan — D. A. Young
J. H. Miller — C. J. Zeller

9. Subcommittee on Watt-hour Meters

W. G. Knickerbocker, *chairman* — H. C. Koenig
A. L. Brownlee — B. E. Kenehan
D. T. Canfield — G. B. M. Robertson (AEIC) ex officio
A. B. Craig — C. F. Savage
F. C. Holtz — M. B. Stout
H. M. Johnson — E. L. Tornquist
L. K. Yerger

Nucleonics

J. J. Smith, *chairman*, General Electric Company, general engineering and consulting laboratory, Schenectady, N. Y.
W. E. Barbour, Jr. — M. M. Hubbard
R. C. Bergvall — Harvard Hull
H. W. Bibber — J. A. Hutcheson
H. W. Brackney — E. H. Krieg
L. J. Bulliet — H. B. Marvin
L. W. Chubb — B. R. Prentice
J. M. Cork — Walther Richter
W. F. Davidson — Frederick Seitz
G. W. Dunlap — Abe Tiles
J. R. Dunning — J. G. Trump
I. A. Getting — R. E. Watson
T. S. Gray — J. T. Wilson

NUCLEONICS SUBCOMMITTEE

1. Nucleonics Instrumentation and Control
G. W. Dunlap, *chairman*

2. Studies of Nuclear Theory
J. T. Wilson, Jr., *chairman*

3. Isotopic Tracers
W. E. Barbour, *chairman*

4. Health Safety and Protection Against Radiation

Therapeutics

E. E. Charlton, *chairman*, General Electric Company, research laboratory, Schenectady 5, N. Y.
F. B. Clausen — G. E. Maxim
H. J. Holmquest — H. D. Moreland
Roy Kegerreis — C. M. Slack
W. B. Kouwenhoven — W. R. Smith
L. Marinelli — L. S. Taylor

General Applications Group

General Applications Co-ordinating Committee

H. C. Coleman, *chairman*, Westinghouse Electric Corporation, East Pittsburgh, Pa.
C. C. Whipple — Elgin B. Robertson

Ex-officio
Chairmen of the five following technical committees in the general applications group

Air Transportation

S. D. Summers, *chairman*, Naval Research Laboratory, Washington 20, D. C.
J. W. Allen — T. J. Martin
G. W. Almassy — C. F. McCabe
J. H. Blankenbuehler — R. S. Mead
H. R. Brown — R. A. Millermaster
W. C. Bryant — J. D. Miner
J. P. Dallas — C. R. Moore
A. Fisher — W. F. Moore
D. E. Fritz — Joseph Nader
C. E. Gagnier — O. F. Olsen
S. H. Hanville, Jr. — W. E. Pakala
W. T. Harding — H. F. Rempt
V. P. Hessler — C. J. Rife
J. G. Hutton — R. A. Rugge
R. R. Jenner — K. R. Smythe
R. D. Jones — S. H. Webster
R. J. Lusk — J. A. White — W. W. West

AIR TRANSPORTATION SUBCOMMITTEES

1. Aircraft Electric Rotating Machinery Subcommittee

Scope: This subcommittee was organized in 1944 for the purpose of preparing standard test codes for aircraft electric rotating machinery. As a result, AIEE 800, Proposed Test Code for D-C Aircraft Machines, was published in March 1947 for one year trial use. The subcommittee now is preparing "Rating Standards or Engine-Driven Generators."

D. E. Fritz, *chairman* — R. D. Jones
J. W. Allen — L. R. Larson
A. Fisher — A. N. Lawrence
S. H. Hanville, Jr. — R. R. Smith — G. W. Sherman

2. Aircraft Systems Subcommittee

Scope: The purpose of this subcommittee is to formulate a report on the basic and accepted considerations involved in the application practice of aircraft electrical systems. This report shall contain information of the following character: an explanation of fundamental electrical problems, outlines of systematic procedures for analysis and computation of aircraft electrical system performance, and formulation of general application practices which will assure correct coordination of equipment.

W. F. Moore, *chairman* — G. A. Phillips
J. W. Allen — D. H. Scott
H. B. Bunce — K. R. Smythe
H. L. Hildebrandt — A. F. Trumbull — A. J. Snyder

3. Subcommittee on Basic Principles of Altitude Rating of Electric Apparatus

Scope: This subcommittee will prepare no test codes nor standards for specific apparatus; but rather, it will prepare a report indicating the basic principles of altitude rating of electric apparatus. Such a report will be useful in the preparation of test codes and standards and in the design and testing of aircraft equipment. The first phase of the project will be from sea level to 50,000 feet altitude.

W. E. Pakala, *chairman* — J. G. Hutton
J. W. Allen — Chester A. Maple
T. R. Brown — A. T. McClinton
S. H. Hanville, Jr. — R. A. Rugge

4. Subcommittee on Aircraft Electrical Control Protective Devices, and Cable

Scope: Formation of test codes and operating standards for aircraft electrical control, protective devices, and cable. The subcommittee is engaged presently in the preparation of a test code for aircraft circuit interrupting devices and a test code for aircraft d-c generator voltage regulators is being prepared by a working group of the subcommittee.

R. J. Lusk, *chairman* — H. S. Moore
B. O. Austin — W. F. Moore
E. E. Magee — J. Ottmar
A. T. McClinton — J. H. Taylor
R. A. Millermaster — W. W. West

Domestic and Commercial Applications

M. M. Brandon, *chairman*, Underwriters Laboratories, Inc., 161 Sixth Avenue, New York 13, N. Y.
S. W. Andrews — C. H. Leatham
D. K. Blake — F. W. Linder
A. C. Bredahl — B. H. Martin
W. B. Buchanan — L. W. McCullough
O. Kendall Buck — K. W. Miller
O. K. Coleman — A. L. Riche
T. H. David, Jr. — C. F. Scott
R. W. Fleming — H. P. Seelye
L. R. Gamble — G. S. Smith
R. R. Herrmann — G. C. Tenney
T. C. Johnson — V. G. Vaughan, Sr.
Wesley Weinerth

Land Transportation

J. G. Aydelott, *chairman*, General Electric Company, locomotive and car equipment divisions, 2901 East Lake Road, Erie, Pa.
L. S. Billau — J. G. Inglis
L. W. Birch — R. L. Kimball
H. R. Blomquist — S. B. Lent
W. A. Brecht — T. M. C. Martin
H. F. Brown — P. A. McGee
D. M. Burckett — E. B. Meissner
C. N. Chase — Ira S. Nippes
W. J. Clardy — A. G. Oehler
Andrew Costic — W. R. Simmons
H. E. Dralle — Dwight L. Smith
Llewellyn Evans — Jacob Stair, Jr.
J. C. Fox — M. C. Swanson
A. P. Frey — R. G. Thring
K. H. Gordon — J. P. Tretton
W. S. H. Hamilton — R. S. Warren
P. H. Hatch — R. A. Williamson
G. L. Hoard — H. P. Wright

LAND TRANSPORTATION SUBCOMMITTEES

1. Heavy Traction Electrification Data Subcommittee

Scope: To collect and tabulate data covering power supply, conversion and distribution systems, and motive power of electrified railroads in the United States and to present them in a way to show the extent of existing electrifications and trends as indicated by chronological developments. Since 1933, general information on railway electrification, both domestic and foreign, has not been available principally because of the discontinuance of the collection of these data by the National Electric Light Association and the disbanding of the heavy electric traction committee of the American Transit Association. In January 1947, this subcommittee was formed and the work is divided into five projects.

L. W. Birch, *chairman* — W. J. Clardy
H. F. Brown — A. G. Oehler — F. H. Craton

Projects:
(*1-1*). Catenary overhead systems
(*1-2*). Third rail systems
(*1-3*). Electric locomotives
(*1-4*). Substations
(*1-5*). Power supply

The personnel of the projects is as follows:
(*1-1*) and (*1-2*). L. W. Birch, *chairman*; H. F. Brown
(*1-3*), (*1-4*), and (*1-5*). A. G. Oehler; F. H. Craton; W. J. Clardy

2. Heavy Traction Papers and Plans Subcommittee

Scope: To secure papers and discussion for the various AIEE meetings, covering matters of current interest in the field relating to railroad electrification, Diesel-electric locomotives, and all other applications of electricity to railroad rolling stock such as lighting and air conditioning of passenger cars. This is accomplished by correspondence and personal contact with the leading manufacturers of equipment, and the engineering personnel of the major electrified railroads and consulting engineering firms engaged in such projects.

H. F. Brown, *chairman* — A. L. Brecht
R. L. Kimball

3. Light Traction Papers and Plans Subcommittee

Scope: To secure papers and discussion for the various AIEE meetings, covering matters of current interest in the field relating to local transit, that is, subway, streetcar, and trolley coach systems, and applications of electric propulsion equipment to internally powered local transit vehicles.

W. J. Clardy, *chairman* — Harold R. Blomquist
Dwight L. Smith

4. Subcommittee on Revision of AIEE Standard 16 (Electric Railway Control Apparatus) ASA C48 Subcommittee

Scope: To revise AIEE Standard 16 (Electric Railway Control Apparatus) for the purpose of recognizing new processes, correction of errors, and inclusions of certain new functions and devices. These Standards last were revised in 1933. The subcommittee reported at the Montreal meeting June 1947 and is expecting to present a final report soon.

F. H. Craton, *chairman* — W. S. H. Hamilton
W. J. Clardy — J. G. Inglis

Marine Transportation

O. A. Wilde, *chairman*, Sun Shipbuilding and Dry Dock Company, Chester, Pa.
E. C. Alger — J. E. Jones
W. B. Armstrong — C. Krommenhock
R. A. Beekman — Clarence Lynn
H. C. Coleman — V. W. Mayer
P. J. DuMont — S. N. Mead
R. W. Erickson — W. H. Reed
J. B. Feder — E. M. Rothen
A. R. Gatewood — L. B. Sinclair
L. M. Goldsmith — C. W. Souder
P. A. Guise — E. H. Stivender
H. F. Harvey, Jr. — G. O. Watson
W. E. Jacobsen — F. A. Wickel
W. N. Zippler

MARINE TRANSPORTATION SUBCOMMITTEES

1. Power Generation Subcommittee

C. Lynn, *chairman* — L. M. Goldsmith
W. B. Armstrong — W. E. Jacobsen
R. A. Beekman — E. H. Stivender
H. C. Coleman — O. A. Wilde

2. Power Application Subcommittee

L. M. Goldsmith, *chairman* — W. E. Jacobsen
W. B. Armstrong — J. E. Jones
H. C. Coleman — C. Lynn
R. W. Erickson — E. H. Stivender

3. Wires and Cables Subcommittee

W. N. Zippler, *chairman* — W. E. Jacobsen
P. J. Dumont — V. W. Mayer

(*Continued on page 1795*)

J. B. Feder — W. H. Reed
A. R. Gatewood — F. A. Wicke

4. Switchboards and Control Subcommittee

H. C. Coleman, *chairman* — J. E. Jones
R. A. Beekman — C. Krommenhock
P. J. Dumont — L. B. Sinclair
H. F. Harvey, Jr. — E. H. Stivender

5. Distribution Subcommittee

H. F. Harvey, Jr., *chairman* — L. M. Goldsmith
W. B. Armstrong — P. A. Guise
R. W. Erickson — V. W. Mayer
A. R. Gatewood — F. A. Wickel
W. N. Zippler

6. Fittings and Appliances Subcommittee

J. B. Feder, *chairman* — H. F. Harvey, Jr.
E. C. Alger — V. W. Mayer
P. J. Dumont — E. M. Rothen
R. W. Erickson — L. B. Sinclair
P. A. Guise — O. A. Wilde

7. Communications and Alarm Subcommittee

W. H. Reed, *chairman* — C. Krommenhock
E. C. Alger — S. N. Mead
J. B. Feder — E. M. Rothen
A. R. Gatewood — L. B. Sinclair
C. W. Souder

8. Navigation Equipment Subcommittee

S. N. Mead, *chairman* — C. Krommenhock
E. C. Alger — E. M. Rothen
P. A. Guise — G. Souder
F. A. Wickel

9. Publicity, Personnel and History Subcommittee

R. A. Beekman, *chairman* — H. F. Harvey, Jr.
W. H. Reed

10. Editing Subcommittee

W. N. Zippler, *chairman* — H. F. Harvey, Jr.
J. B. Feder — W. H. Reed

Production and Application of Light

Harris Reinhardt, *chairman*, Sylvania Electric Products Company, 500 Fifth Avenue, New York 18, N. Y.

J. F. Angier — J. A. McDermott
L. H. Brown — F. H. Murphy
M. J. Dring — R. L. Oetting
Frank Hansen — R. C. Putnam
C. P. Hayes — D. W. Rowten
S. G. Hibben — E. H. Salter
O. W. Holden — R. G. Slauer
E. A. Linsday — H. F. Wall
H. E. Mahan — E. S. West
C. C. Whipple

PRODUCTION AND APPLICATION OF LIGHT SUBCOMMITTEES

1. Ultraviolet Radiations Subcommittee

Scope: The initial scope will be to sponsor the preparation of a report on modern ultraviolet sources. The group also would serve as a clearing house for all information relating to its specific field, and, will help in planning convention papers and securing speakers on this subject.

Allen J. Dusault, *chairman*

2. Nomenclature of Electronic Lamps Subcommittee

Scope: This subcommittee was formed for the purpose of co-operating with the subcommittee on electronic standards of the committee on electronics in the preparation of a system of definitions of electronic devices used as light sources. In this connection, light sources is taken as including sources of infrared, visible, and ultraviolet radiations.

E. H. Salter, *chairman* — L. R. Keifter
E. W. Beggs — R. C. Putnam
T. G. Sargent

3. Infrared Radiations Subcommittee

E. A. Lindsay, *chairman* — M. J. Bevins — G. Parmallee

Joint Subcommittees

Joint Subcommittee on Application of Probability Methods to Capacity Problems (Joint with Power Generation and System Engineering Committees)

G. Calabrese, *chairman* — H. W. Phillips
W. R. Blakeley — C. A. Roberts
H. Duryea — H. B. Smith
R. G. Hooke — H. P. St. Clair
H. A. Lott — G. M. Tatum
W. J. Lyman — V. A. Thiemann
K. W. Miller — C. W. Watchorn

Joint Subcommittee on Carbon Brushes (Joint with Rotating Machinery and Air Transportation Committees)

V. P. Hessler, *chairman* — H. E. Kenelpp
W. H. Autry — E. A. Lapham
H. R. Brown — T. M. Linville
J. V. Dobson — D. Ramadanoff
H. M. Elsey — Orrin Rutledge
C. J. Herman — S. D. Summers
L. H. Hilderbrandt — Atten: A. L. Van Emdon (Navy Department)

Joint Subcommittee on Standard Frequency Bands and Designations

Scope: To consult with various interested groups and recommend designated bands which conform, as closely as possible, with the desires and needs of the majority. A logarithmic system of bands seems to be indicated to break up this enormous range from a small fraction of a cycle per second to 10^{18} cycles per second or higher into bands which can be designated readily.

Thomas Spooner, *chairman* (representative, electronic standards subcommittee)
C. T. Burke (representative, instruments and measurements committee)
G. B. Ransom (representative, communication committee)
F. B. Silsbee (representative, ASA sectional committee C88)
J. E. McCormack (representing ASA Electric Light and Power Group)

Joint Subcommittee on Nucleonic Instruments (Joint with Nucleonics and Instruments and Measurements Committees)

Scope: Treatment of all matters pertaining to the design, construction, and application of devices used for the detection and measurement of nucleonic radiations. The activity of the subcommittee in this field will include:

1. Sponsorship of papers of two general kinds, namely, survey papers of a broad educational nature and descriptive papers covering developments primarily of interest to the membership.

2. Sponsorship of conference sessions on similar subjects.

3. Co-operation with the various existing committees of the AIEE and other organizations, particularly with respect to standardization and definitions. (In this latter instance, it is believed this subcommittee's efforts initially will be by way of review and suggestion.)

G. W. Dunlap, *chairman* — J. A. Hutcheson
E. E. Goodale, *secretary* — P. S. Johnson
William E. Barbour, Jr. — J. B. H. Kuper
L. F. Curtiss — Frank D. Lewis
F. J. Gaffney — G. A. Morton
W. H. Hall — H. W. Newson
M. M. Hubbard — M. A. Schultz
H. G. Weiss

Joint Subcommittee on Electronic Instruments (Joint with Electronics and Instruments and Measurements)

J. G. Reid, Jr., *chairman* — G. B. Hoadley
Rudolf Feldt, *vice-chairman* — Eric Isbister
W. H. Tidd, *secretary* — G. N. Mahaffey
H. W. Berry — C. W. Martel
L. T. Bourland — H. R. Meahl
C. T. Burke — David Packard
C. C. Chambers — T. B. Perkins
W. R. Clark — J. J. Slattery
H. F. Dart — E. R. Thomas
H. C. Drake — J. T. Thwaites
G. V. Elgroth — V. Ulrich
D. B. Fisk — A. H. Waynick
W. A. Geohegan — A. J. Williams, Jr.
E. R. Haberland — W. P. Wills

Present Activities:

1. Continuing a program of survey, information collation, and liaison among instrument manufacturers, tube manufacturers, and other groups interested in improving the performance of electron tubes in instrument and industrial applications.

2. To solicit, review, and sponsor technical papers and reports on electronic instruments.

Proposed Future Activities:

1. In the field of electronic instrumentation, an investigation of desirable characteristics for special purpose electron tubes (photoelectric tubes, cathode ray tubes, and so forth).

2. Investigation of desirable characteristics for circuit components to be used in instruments in conjunction with electron tubes (sockets, potentiometers, and so forth).

3. Investigation of the need for definitions and standards pertaining to electronic instruments.

Joint Subcommittee on Telemetering (Joint with Substation and Instruments and Measurements Committee)

Scope: Telemetering is assigned primarily to the committee on instruments and measurements and handled by a joint subcommittee comprised of members of both the committees on substations and on instruments and measurements. Telemetering problems in connection with automatic stations will be assigned by the chairman of the committee on instruments and measurements to the substation committee. Telemetering problems of a more general nature will be assigned by him to members of both main committees and where the problem is simply measurements it will be assigned only to members of the committee on instruments and measurements.

Substation Appointees:

W. A. Derr — M. W. Keck (representative, carrier current committee)
W. L. Hiscox, *secretary*
George S. Lunge — J. H. Vivian
A. P. Peterson — C. E. Winegartner

Instruments and Measurements Appointees:

C. K. Duff, *chairman* — J. T. Logan
P. A. Borden — E. E. Lynch
M. A. Bostwick — W. E. Phillips
E. C. Brown — G. M. Thynell

Institute Representatives

Aeronautical Electrical Equipment Standardization
S. D. Summers, liaison representative with NASC and SAE

Alfred Noble Prize Committee, ASCE
T. M. Linville

American Association for the Advancement of Science, Council
J. W. Barker — I. Melville Stein

American Research Committee on Grounding
C. T. Sinclair

American Standards Association, Standards Council
F. E. Harrell — R. T. Henry
Alternates
R. C. Bergvall — J. R. North — E. B. Paxton

American Year Book, Advisory Board
H. H. Henline

Construction Industry Advisory Council
Henry A. Lardner

Electrical Standards Committee, ASA
H. E. Farrer — E. B. Paxton
Alternates
W. P. Dobson — F. E. Harrell — J. J. Pilliod

Engineering Foundation Board
J. W. Barker — C. G. Suits

Engineering Societies Monographs Committee
F. M. Farmer — W. I. Slichter

Engineering Societies Personnel Service, Inc.
H. H. Henline

Engineers' Council for Professional Development
M. S. Coover — E. W. Davis — M. D. Hooven

Engineers Joint Council
H. H. Henline — J. E. Housley — B. D. Hull
Everett S. Lee, *ex-officio*

EJC Committee on International Relations
H. H. Henline — A. C. Monteith

EJC Committee on Selective Service
Mervin J. Kelly

EJC National Engineers Committee, Consultative to Federal Authorities
Elmer Housley — L. N. McClellan

General Electric Educational Fund Fellowship Committee
Everett S. Lee

Herts Award Committee
M. S. Coover

Hoover Medal Board of Award
H. S. Osborne — John Castlereagh Parker — J. G. Tarboux

Industry Committee on Interior Wiring Design
M. M. Brandon — L. C. Peterman

John Fritz Medal Board of Award
N. E. Funk — J. Elmer Housley — B. D. Hull
Everett S. Lee

Joint Committee for Development of Statistical Applications in Engineering and Manufacturing
W. P. Dobson

Library Board, United Engineering Trustees, Inc.
George Sutherland — H. M. Turner
Alternate
J. F. Fairman

Marston Medal Board of Award
Harry B. Gear

National Association of Corrosion Engineers: Intersociety Committee on Corrosion
L. J. Gorman — H. M. Trueblood

National Bureau of Engineering Registration, Advisory Board
J. E. Hobson

National Electronics Conference
W. G. Dow

National Fire Protection Association, Electrical Committee
Robin Beach

National Fire Waste Council
Robin Beach W. R. Smith

National Research Council, Division of Engineering and Industrial Research
W. B. Kouwenhoven

National Technological Advisory Committee
John Castlereagh Parker

Quarterly of Applied Mathematics
M. G. Malti

United Engineering Trustees, Inc.
J. F. Fairman W. H. Harrison D. A. Quarles

United States National Committee of the International Commission on Illumination
Ward Harrison S. G. Hibben Harris Reinhardt

United States National Committee of the International Electrotechnical Commission
H. E. Farrer K. B. Paxton
Alternates
W. P. Dobson F. E. Harrell J. J. Pilliod

Washington Award Commission
H. B. Gear Frank V. Smith

World Power Conference, Executive Committee of United States National Committee
Everett S. Lee

Student Branches

Name and Location	District	Counselor (Member of Faculty)
Akron, University of, Akron, Ohio	2	K. F. Sibila
Alabama Polytechnic Inst., Auburn	4	R. D. Spann
Alabama, University of, University	4	W. F. Gray
Alberta, University of, Edmonton, Canada	10	R. E. Phillips
Arizona, University of, Tucson	8	J. C. Clark
Arkansas, Univ. of, Fayetteville	7	N. H. Barnette
British Columbia, Univ. of, Vancouver, Canada	10	W. B. Coulthard
Brooklyn Polytechnic Inst. of, Brooklyn, N. Y. (Day)	3	F. A. Wahlers
Brooklyn Polytechnic Inst. of, Brooklyn, N. Y. (Evening)	3	Anthony B. Giordano
Brown University, Providence, R. I.	1	F. N. Tompkins
Bucknell University, Lewisburg, Pa.	2	G. A. Irland
California Inst. of Technology, Pasadena	8	William H. Pickering
California, University of, Berkeley	8	R. M. Saunders
Carnegie Inst. of Technology, Pittsburgh, Pa.	2	E. M. Williams
Case Inst. of Technology, Cleveland, Ohio	2	Carl F. Schuneman
Catholic Univ. of America, Washington, D. C.	2	J. C. Michalowicz
Cincinnati, Univ. of, Cincinnati, Ohio	2	A. C. Herweh
Clarkson College of Technology, Potsdam, N. Y.	1	Alfred R. Powers
Clemson A. & M. College, Clemson, S. C.	4	F. T. Tingley
Colorado A. & M. College, Fort Collins	6	C. H. Chinburg
Colorado, University of, Boulder, Colo.	6	Platt Wicks
Columbia University, New York, N. Y.	3	W. A. LaPierre
Connecticut, Univ. of, Storrs	1	L. E. Williams
Cooper Union, New York, N. Y.	3	E. W. Starr
Cornell University, Ithaca, N. Y.	1	M. S. McIlroy
Delaware, University of, Newark	2	R. E. Kuehn
Denver, University of, Denver, Colo.	6	A. M. Stiles
Detroit, University of, Detroit, Mich.	5	H. O. Warner
Drexel Inst. of Technology, Philadelphia, Pa.	2	F. C. Powell
Duke University, Durham, N. C.	4	Otto Meier, Jr.
Florida, University of, Gainesville	4	F. H. Pumphrey
George Washington Univ., Washington, D. C.	2	J. S. Antel, Jr.
Georgia Inst. of Technology, Atlanta	4	H. B. Duling
Harvard University, Cambridge, Mass.	1	John C. Fisher
Idaho, University of, Moscow	9	J. Hugo Johnson
Illinois Inst. of Technology, Chicago	5	E. T. B. Gross
Illinois, University of, Urbana	5	E. A. Reid
Iowa State College, Ames	5	G. Richardson
Iowa, University of, Iowa City	5	E. B. Kurtz
Johns Hopkins University, Baltimore, Md.	2	T. Benjamin Jones
Kansas State College, Manhattan	7	Joe E. Ward
Kansas, University of, Lawrence	7	D. G. Wilson
Kentucky, University of, Lexington	4	Brinkley Barnett
Lafayette College, Easton, Pa.	2	Finley W. Smith
Lehigh University, Bethlehem, Pa.	2	J. L. Beaver
Louisiana State University, Baton Rouge	4	L. V. McLean
Louisville, University of, Louisville, Ky.	4	T. R. Bailey
Maine, University of, Orono	1	P. M. Seal
Manhattan College, New York, N. Y.	3	Robert T. Weil
Marquette University, Milwaukee, Wis.	5	E. W. Kane
Maryland, University of, College Park	2	L. J. Hodgins
Massachusetts Inst. of Technology, Cambridge	1	E. W. Boehne
Michigan College of Mining & Technology, Houghton	5	G. W. Swenson
Michigan State College, East Lansing	5	C. E. Goodell
Michigan, University of, Ann Arbor	5	J. S. Gault
Milwaukee School of Engineering, Milwaukee, Wis.	5	R. J. Ungrodt
Minnesota, University of, Minneapolis	5	J. H. Kuhlmann
Mississippi State College, State College	4	Harry C. Simrall
Missouri School of Mines & Metallurgy, Rolla	7	I. H. Lovett
Missouri, University of, Columbia	7	J. F. Lamb
Montana State College, Bozeman	9	E. W. Schilling
Nebraska, University of, Lincoln	6	C. W. Rook
Nevada, University of, Reno	8	I. J. Sandorf
Newark College of Engineering, Newark, N. J.	3	C. R. Moore
New Hampshire, University of, Durham, N. H.	1	R. E. Anderson
New Mexico College of A. & M. Arts, State College	7	Russell L. Reise
New Mexico, University of, Abuquerque	7	Ralph W. Tapy
New York, College of the City of, New York, N. Y.	3	Harry Baum
New York University, New York	3	S. G. Lutz
North Carolina State College, Raleigh	4	W. D. Stevenson, Jr.
North Dakota State Agrl. College, Fargo	5	Harry S. Dixon
North Dakota, University of, Grand Forks	5	K. B. MacKichan
Northeastern University, Boston, Mass.	1	Roland G. Porter
Northwestern University, Evanston, Ill.	5	A. H. Wing
Norwich University, Northfield, Vt.	1	F. A. Spencer
Notre Dame, University of, Notre Dame, Ind.	5	L. F. Stauder
Ohio Northern University, Ada	2	
Ohio State University, Columbus	2	Edmund D. Ayres
Ohio University, Athens	2	D. B. Green
Oklahoma A. & M. College, Stillwater	7	Charles F. Cameron
Oklahoma, University of, Norman	7	Gerald Tuma
Oregon State College, Corvallis	9	Melvin J. Kofoid
Pennsylvania State College, State College	2	A. P. Powell
Pennsylvania, University of, Philadelphia	2	Kenneth Fegley
Pittsburgh, University of, Pittsburgh, Pa.	2	R. C. Gorham
Pratt Institute, Brooklyn, N. Y.	3	
Princeton University, Princeton, N. J.	2	H. C. Rentschler
Puerto Rico, University of, Mayaguez, P. R.	3	Miguel Wiewall, Jr.
Purdue University, Lafayette, Ind.	5	G. R. Nichols
Rensselaer Polytechnic Institute, Troy, N. Y.	1	Emerson D. Broadwell
Rhode Island State College, Kingston	1	J. L. Hummer
Rice Institute, Houston, Tex.	7	J. S. Waters
Rose Polytechnic Institute, Terre Haute, Ind.	5	C. C. Knipmeyer
Rutgers University, New Brunswick, N. J.	3	P. S. Creager
Santa Clara, Univ. of, Santa Clara, Calif.	8	Henry P. Nettesheim
South Carolina, Univ. of, Columbia	4	S. A. Ferguson
South Dakota School of Mines & Tech., Rapid City	6	J. O. Kammerman
South Dakota State College, Brookings	5	Wm. H. Gamble
Southern California, Univ. of, Los Angeles	8	Rodney C. Lewis
Southern Methodist Univ., Dallas, Tex.	7	F. W. Tatum
Stanford University, Stanford University, Calif.	8	Wm. G. Hoover
Stevens Inst. of Technology, Hoboken, N. J.	3	W. L. Sullivan
Swarthmore College, Swarthmore, Pa.	2	C. A. Lister
Syracuse University, Syracuse, N. Y.	1	Edward Shelton
Tennessee, University, Knoxville	4	W. O. Leffell
Texas, A. &. M. College of, College Station	7	N. F. Rode
Texas Technological College, Lubbock	7	T. B. Stenis
Texas, University of, Austin	7	A. J. McCrocklin, Jr.
Toronto, University of, Toronto, Ont., Canada	10	L. S. Lauchland
Tufts College, Medford, Mass.	1	A. H. Howell
Tulane University, New Orleans, La.	4	Albert C. Buxton
Union College, Schenectady, N. Y.	1	Owen G. Owens
Utah, University of, Salt Lake City	9	O. C. Haycock
Vanderbilt University, Nashville, Tenn.	4	S. R. Schealer
Vermont, University of, Burlington	1	E. R. McKee
Villanova College, Villanova, Pa.	9	J. B. Clothier
Virginia Military Institute, Lexington	4	J. S. Jamison
Virginia Polytechnic Institute, Blacksburg	4	Claudius Lee
Virginia, University of, Charlottesville	4	L. R. Quarles
Washington, State College of, Pullman	9	O. E. Osburn
Washington, University of, Seattle	9	Vinson L. Palmer
Washington University, St. Louis, Mo.	7	Pierre M. Honnell
Wayne University, Detroit, Michigan	5	W. H. Bixby
West Virginia University, Morgantown	2	Everette C. Dubbe
Wisconsin, University of, Madison	5	John C. Weber
Worcester Poly. Institute, Worcester, Mass.	1	Donald C. Alexander
Wyoming, University of, Laramie	6	R. O. Trueblood
Yale University, New Haven, Conn.	1	A. G. Conrad

Total Branches.....127

Sections

Name	District	When Organized	Membership Aug. 1, 1948	Chairman	Secretary	Secretary's Address
Akron	2	Aug. 12, '20	109	C. R. Reid	N. A. Williams	Goodyear Tire & Rubber Co., Dept. 110-X, Akron, Ohio
Alabama	4	May 22, '29	158	H. J. Scholz	J. I. Greenhill	1316 46th St., West, Birmingham 8, Ala.
Arizona	8	Mar. 22, '41	112	C. M. Hart	Americo Lazzari	4323 North 14th Ave., Phoenix, Ariz.
Arkansas	7	Apr. 23, '47	77	H. W. Claybaugh	T. B. Lewis	6609 Kenwood Road, Little Rock, Ark.
Arrowhead	5	Apr. 23, '47	39	R. L. Boisen	J. F. Johnson	Minnesota Power & Light Co., Duluth, Minn.
Beaumont	7	June 27, '45	78	J. W. Millington	Harold E. Mortimer	Gulf States Utilities Co., Beaumont, Texas
Boston	1	Feb. 13, '03	803	F. B. Haeussler	L. F. Cleveland	Northeastern University, 360 Huntington Ave., Boston 15, Mass.
Canton	2	Nov. 5, '47	69	D. C. Krammes	W. D. Cropley	5436 Jonathan Ave., N. W., Canton 6, Ohio
Central Indiana	5	Jan. 12, '12	222	G. F. Switzer	G. M. Grabbe	4507 East 10th St., Indianapolis 1, Ind.
Chicago	5	1893	1,487	F. D. Troxel	H. E. Nason	Westinghouse Elec. Corp., 20 North Wacker Drive, Chicago 6, Ill.
Cincinnati	2	June 30, '20	205	Ray E. Stroppel	John P. Quitter	509 Missouri Ave., Cincinnati 26, Ohio
Cleveland	2	Sept. 27, '07	629	J. D. Leitch	J. L. Fuller	Reliance Elec. & Engg. Co., 1088 Ivanhoe Road, Cleveland 10, Ohio
Columbus	2	Mar. 17, '22	131	Chester K. Bishop	Roger L. Merrill	691 North Dawson Ave., Columbus 3, Ohio
Connecticut	1	Apr. 16, '21	492	Clarence W. Mayott	Floyd W. Buck	United Illuminating Co., 80 Temple St., New Haven, Conn.
Dayton	2	June 9, '43	300	W. R. Appleman	C. H. Spitler	1544 Huffman Ave., Dayton 3, Ohio
Denver	6	May 18, '15	358	C. W. Keller	Evan R. Jones	Mountain States Tel. & Tel. Co., Box 960, Denver 1, Colo.
East Tennessee	4	Sept. 2, '36	342	R. W. McEver	Henry E. Williams	Knox Porcelain Corp., 100-200 Mynderse St., Knoxville 11, Tenn.
Erie	2	Jan. 11, '18	120	Charles L. Reed, Jr.	F. T. Parker	2304 Bird Drive, Erie, Pa.
Florida	4	Jan. 28, '31	225	R. S. Davis	Jess D. Thomas	904 Ethan Allen, Jacksonville, Florida
Fort Wayne	5	Aug. 14, '06	114	C. J. Herman	R. E. Trovinger	2703 Hoagland Ave., Fort Wayne 6, Ind.
Georgia	4	Jan. 14, '04	207	R. O. Loomis	James R. Raney	Georgia Power Co., 75 Marietta St., N. W., Atlanta, Ga.
Houston	7	Aug. 7, '28	246	H. C. Dillingham	E. G. Westheimer	Southwestern Bell Tel. Co., Box 1780, Houston 2, Texas
Illinois Valley	5	June 30, '45	117	Walter J. Johnson	George E. Walters	707 East Lawndale Ave., Peoria 4, Ill.
Iowa	5	June 29, '29	159	John V. Gebuhr	John H. O'Day	Northwestern Bell Tel. Co., Des Moines, Iowa
Ithaca	1	Oct. 15, '02	158	P. D. Ankrum	E. M. Guyer	Corning Glass Works, Corning, N. Y.
Kansas City	7	Apr. 14, '16	228	J. E. Murray	Ivan T. Knight	Kansas City Power & Light Co., 1330 Baltimore Ave., Kansas City 10, Mo.
Lehigh Valley	2	Apr. 16, '21	281	J. E. Treweek	A. L. Price	173 South Church St., Hazleton, Pa.
Los Angeles	8	May 19, '08	1,179	Bradley Cozzens	J. H. Vivian	301 No. Gerona Ave., San Gabriel, Calif.
Louisville	4	Oct. 15, '26	110	M. G. Northrop	R. D. Spalding	2595 Greenup, Louisville 4, Ky.
Lynn	1	Aug. 22, '11	228	R. G. Slauer	R. E. Franck	General Elec. Co., 40 Federal St., West Lynn, Mass.
Madison	5	Jan. 8, '09	82	Leonard Hesse	Rubin A. Imm	102 Electrical Laboratory, University of Wisconsin, Madison, Wis.
Mansfield	2	Mar. 6, '39	68	C. W. Freeman	H. R. Delahooke	North Elec. Mfg. Co., Galion, Ohio
Maryland	1	Dec. 16, '04	536	Robert L. McCoy	John W. Gore	3707 Woodbine Ave., Baltimore 7, Md.
Memphis	4	May 22, '30	106	James R. Morton	Wm. B. Thompson	Firestone Tire & Rubber Co., Firestone Blvd., Memphis 7, Tenn.
Mexico	7	June 29, '22	202	E. D. Luque	Francisco Aubert	Apartado Postal 1399, Mexico D. F., Mexico
Miami	4	Feb. 3, '49	96	Harvey F. Pierce	Clyde V. Booth	200 Langford Bldg., Miami 32, Fla.
Michigan	5	Jan. 13, '11	695	M. R. Horne	E. Dismeyer	Consumers Power Bldg., Jackson, Mich.
Milwaukee	5	Feb. 11, '10	621	E. T. Sherwood	E. J. Limpel	A. O. Smith Corp., Milwaukee, 1, Wis.
Minnesota	5	Apr. 7, '02	240	Harry P. Brunckc	Albert J. Hendry	Northern Pacific Ry. Co., St. Paul 1, Minn.
Montana	9	June 24, '31	84	Sidney McArthur	Dennis A. Johnson	Mountain States Tel. & Tel. Co., Great Falls, Mont.
Montreal	10	Apr. 16, '43	308	M. C. Thurling	A. Malkin	Canadian Car & Foundry Co. Ltd., 621 Craig St., West, Montreal, P.Q.3, Canada
Muscle Shoals	4	Feb. 18, '38	23	L. W. Jenkins	Arthur R. Rifkin	Apt. 4, Spring Creek Apts., Sheffield, Ala.
Nebraska	6	Jan. 21, '25	85	A. A. Little	Claude N. Allen	Allis-Chalmers Mfg. Co., 9th & Farnam Sts., Omaha 8, Nebr.
New Mexico–West Texas	7	Mar. 7, '40	108	O. F. Metz	Lee R. Hammond, Jr.	2910 Grant Ave., El Paso, Texas
New Orleans	4	Dec. 8, '33	210	M. G. Zervigon	E. I. Blanchard	Louisiana Power & Light Co., 433 Metairie Road, New Orleans, 20, La.
New York	3	Dec. 10, '19	4,715	R. T. Oldfield	J. D. Tebo	Bell Telephone Labs., Inc., 463 West St., New York 14, N. Y.
Niagara Falls		Aug. 5, '48	79	W. Lore Eliason	John A. Persson	P.O. Box 580, Union Carbide & Carbon Research Lab., Inc., Niagara Falls, N. Y.
Niagara Frontier	1	Feb. 10, '25	249	John E. Sowers	Berton S. Rice	Buffalo Niagara Elec. Corp., Electric Bldg., Buffalo 3, N. Y.
North Carolina	4	Mar. 21, '29	206	C. G. Brennecke	W. D. Stevenson, Jr.	North Carolina State College of Agri. & Engg., Raleigh, N. C.
North Texas	7	May 18, '28	307	J. M. Engler	P. G. Wallace	Texas Power & Light Co., P.O. Box 6331, Dallas 2, Texas
Oklahoma City	7	Feb. 16, '22	168	Paul Berry	Harold L. Pickens	Allis-Chalmers Mfg. Co., 901 West Grand, Oklahoma City 4, Okla.
Panhandle Plains	7	June 12, '47	89	R. F. McClure	R. P. Miller	City of Lubbock, Lubbock, Texas
Philadelphia	2	Feb. 18, '03	1,248	A. P. Godsho	H. H. Sheppard	Rumsey Elec. Co., 1007 Arch St., Philadelphia 7, Pa.
Pittsburgh	2	Oct. 13, '02	916	H. H. Wagner	F. H. Schlough	416 7th Ave., Pittsburgh 19, Pa.
Pittsfield	1	Mar. 25, '04	240	Horace S. Hubbard	J. H. Hagenguth	General Elec. Co., 100 Woodlawn Ave., Pittsfield, Mass.
Portland	9	May 18, '09	407	M. M. Ewell	Howard Arnett	Portland General Elec. Co., 621 S.W. Alder St., Portland 5, Oreg.
Providence	1	Mar. 12, '20	129	William B. Carnie	Chester B. Leathers	General Elec. Co., 111 Westminster St., Providence, R. I.
Richland	9	Apr. 23, '48	61	H. R. Hughes	J. H. Hemperly	General Elec. Co., Project Engg. Div., Richland, Wash.
Rochester	1	Oct. 9, '14	204	C. G. Plomasen	John H. Rogers	Eastman Kodak Co., Camera Works, Rochester 4, N. Y.
Rock River Valley	5	Apr. 23, '47	69	Warner A. Johnson	Sheldon A. Coxhead	Central Illinois Elec. & Gas Co., 303 N. Main St., Rockford, Ill.
St. Louis	7	Jan. 14, '03	446	R. W. Gaskins	E. E. Gilcrease	4930 San Francisco Ave., St. Louis 15, Mo.
San Diego	8	Jan. 18, '39	150	H. A. Cordes	J. P. Conner	5040 Westminster Terrace, San Diego 4, Calif.
San Francisco	8	Dec. 23, '04	1,137	O. A. Gustafson	W. L. Carter	Pacific Tel. & Tel. Co., 140 New Montgomery St., San Francisco 5, Calif.
Schenectady	1	Jan. 26, '03	839	H. L. Palmer	L. T. Rader	General Elec. Co., Control Engineering Div., Schenectady 5, N. Y.
Seattle	9	Jan. 19, '04	374	Harold R. Brown	F. R. Bergseth	Dept. of Elec. Engg., University of Washington, Seattle 5, Wash.
Sharon	2	Dec. 11, '25	158	E. W. Tipton	G. R. Monroe	1108 Memorial Blvd., Sharon, Pa.
Shreveport	4	June 12, '47	65	R. O. Williams	L. T. Williams	P.O. Box 1106, Shreveport, La.
South Bend	5	Feb. 26, '41	95	R. M. Koontz	C. O. Moyer	1134 Lincolnway, West, South Bend, Ind.
South Carolina	4	Mar. 2, '40	86	Max G. Toole	E. Parker Miller	South Carolina Elec. & Gas Co., P.O. Box 390, Columbia, S. C.
South Texas	7	May 23, '30	131	M. F. Nonier	A. C. Alberti	City Public Service Board, 201 No. St. Mary's St., San Antonio 6, Texas
Spokane	9	Feb. 14, '13	125	O. W. Hurd	S. J. Pope	952 East 40th Ave., Spokane 10, Wash.
Springfield	1	June 29, '22	93	J. L. Hyland	D. W. Hoot	United States Rubber Co., Chicopee, Mass.
Syracuse	1	Aug. 12, '20	203	L. M. Moore	J. D. Hershey	General Elec. Co., 113 South Salina St., Syracuse 2, N. Y.
Toledo	2	June 3, '07	103	M. W. Keck	Walter M. Campbell	2145 Central Grove Ave., Toledo, Ohio
Toronto	10	Sept. 30, '03	509	J. F. Moore	W. R. Harmer	Hydro Elec. Power Comm. of Ontario, 620 University Ave., Toronto 2, Ont.
Tulsa	7	Oct. 1, '37	101	Sim C. Wright	George R. Van Burkleo	Public Service Co., 600 South Main, Tulsa 1, Okla.
Urbana	5	Nov. 25, '02	88	A. D. Bailey	D. F. Hang	Dept. of Elec. Engg., University of Illinois, Urbana, Ill.
Utah	9	Mar. 9, '17	110	Thomas Jordan	John A. McDonald	General Elec. Co., P.O. Box 779, Salt Lake City, 1, Utah
Vancouver	10	Aug. 22, '11	167	J. H. Steede	D. S. Smith	3820 Dunbar, Vancouver, B. C., Canada
Virginia	4	May 19, '22	210	Wm. T. Johns	P. R. Sprachcr	P.O. Box 1194, Richmond 19, Va.

Sections (*Continued*)

Washington	2	Apr. 9, '03	790	R. D. Bennett	W. J. Lank	Potomac Elec. Power Co., 10th & E Sts. N.W., Washington 4, D. C.
West Virginia	2	Apr. 9, '40	102	T. R. Cooper	James L. Simpson	10 Brookland Court, Charleston 1, W. Va.
Wichita	7	Sept. 16, '37	86	E. D. Manspeaker	J. E. McNicol	Kansas Gas & Elec. Co., 201 No. Market, Wichita 2, Kansas
Worcester	1	Feb. 18, '20	81	Douglas L. Watkins	Wilfred G. Coleman	General Elec. Co., 507 Main St., Worcester 8, Mass.
Total Section	83		27,667			

Subsections

Name	Chairman	Secretary	Secretary's Address
Baton Rouge (New Orleans Section)	S. J. Helfman	F. B. Avery	Southern Bell Tel. & Tel. Co., 555 Florida St., Baton Rouge 8, La.
Billings (Montana Section)	John R. Walker	Frank M. Holly	545 Park Place, Billings, Mont.
Binghamton Area (Ithaca Section)	J. R. Stover	W. J. Rider	P.O. Box 1081, Binghamton, N. Y.
Boulder City (Los Angeles Section)	Albert E. Hamilton	William M. Doak	P.O. Box 398, Henderson, Nevada
Casper, Wyo. (Denver Section)	R. M. Sensintaffar	C. R. McLean	Mountain States Power Co., Casper, Wyo.
Central Texas (North Texas Section)	W. D. Nuckols	A. E. Vickery	710 North 11th Street, Waco, Tex.
Centre County (Pittsburgh Section)	R. E. Armington	W. B. Sheppard	Elec. Engg. Dept., Pennsylvania State College, State College, Pa.
Charleston (South Carolina Section)	George Dibble	E. B. Woodward	1007 Montague Ave., North Charleston, S. C.
Charlotte (North Carolina Section)	Ben Martin	M. F. Leftwich	Duke Power Co. 1969, Charlotte, N. C.
Columbia (South Carolina Section)	J. F. Rader, Jr.	S. A. Ferguson	2329 Terrace Way, Columbia 41, S. C.
Corpus Christi (South Texas Section)	M. H. Lovelady	M. D. Jester	3629 Austin St., Corpus Christi, Texas
Forth Worth (North Texas Section)	F. E. Woodruff	C. K. Beyette	2304 Loraine Court, Fort Worth 3, Texas
Fox River Valley (Milwaukee Section)	Jesse W. Pomazal	Joseph J. Rock	Giddings & Lewis, Fond du Lac, Wis.
Freeport (Houston Section)	B. J. Nankervis	H. K. Brown	1414 West 5th St., Freeport, Texas
Fresno (San Francisco Section)	F. E. Miller	Clyde L. Smith	Pacific Gas & Electric Co., 1401 Fulton St., Fresno 24, Calif.
Great Falls (Montana Section)	Sidney E. McArthur	D. A. Johnson	Box 1689, Great Falls, Mont.
Hamilton (Toronto Section)	C. E. Moorhouse	E. S. Webb	Canadian Westinghouse Co., Hamilton, Ont., Canada
Hampton Roads (Virginia Section)	L. Saunders	K. O. Joyner	45 Algonquin, Hampton, Va.
Hudson Valley (New York Section)	M. S. Kozik	E. W. Paquin	Central Hudson Gas & Elec. Corp., Poughkeepsie, N. Y.
Jackson (Miss.) (New Orleans Section)	Joseph B. Fountain	A. N. Saxon	Graybar Elec. Co., 758 Ricks St., Jackson, Miss.
Jacksonville (Florida Section)	R. M. Garth		
Johnstown (Pittsburgh Section)	A. M. Baker	W. M. Rohrer	Johnstown Center, University of Pittsburgh, Johnstown, Pa.
Lake Charles (New Orleans)	J. W. Kirkland	W. W. Crossley	1317 Shaw St., Lake Charles, La.
Lancaster–York (Maryland Section)	J. L. Stauffer	W. H. Freund	RCA Mfg. Co., Lancaster, Pa.
Lima (Dayton Section)	L. C. Schaefer	O. M. Swain	Westinghouse Electric Corp., Lima, Ohio
Miami (Florida Section)	Harvey F. Pierce	C. V. Booth	c/o M. H. Connell & Associates, Langford Bldg., Miami 32, Fla.
Mid-State (North Carolina Section)	F. R. Jackson, Jr.	L. H. Allcorn, Jr.	2425 Elizabeth Ave., Winston-Salem 7, N. C.
New Hampshire (Boston Section)	L. H. Faulkingham	H. K. Coates	273 Main St., Nashua, N. H.
New Jersey (New York Section)	Lawrence J. Lunas	J. L. Blackburn	Westinghouse Electric Corp., Newark 1, N. J.
Oak Ridge (East Tennessee Section)		M. P. Falls	105 Vermont Ave., Oak Ridge, Tenn.
Ottawa (Montreal Section)	G. R. Davis	R. M. Morris	National Research Council, Sussex St., Ottawa, Ont., Canada
Quad Cities (Iowa Section)	Raymond C. Judd	E. H. Breckenfelder	P.O. Box 537, Muscatine, Iowa
Red River Valley (Minnesota Section)	Thomas C. Wright	G. O. Gerth	Otter Tail Power Co., Fergus Falls, Minn.
Richmond (Virginia)	Bruce G. Halsted	C. E. McMurdo	3523 Patrick Ave., Richmond 22, Va.
Sacramento (San Francisco Section)	E. S. Prud'Homme	Charles R. Day	Sacramento Municipal Utility Dist., P.O. Box 2391, Sacramento, Calif.
St. Maurice Valley (Montreal Section)	J. J. Dupuis	Donald King	P.O. Box 70, Shawinigan Falls, Que., Canada
Saginaw Valley (Michigan Section)	H. R. Walker	E. L. Holmgren	400 Park Ave., Bay City, Mich.
Sangamon (Illinois Valley Section)	W. W. Sherwood	J. F. Kurzska	Central Illinois Public Service Co., Illinois Bldg., Springfield, Ill.
San Jose (San Francisco Section)	J. Scott Kay	L. O. Templeton	Westinghouse Electric Corp., Project 28, Sunnyvale, Calif.
Shasta (San Francisco Section)	M. E. Guile	W. G. Whitney	Pacific Gas & Electric Co., 301 Main St., Chico, Calif.
Tampa (Florida Section)	R. S. Davis		
Tri-State (West Virginia Section)	H. F. Rosier	G. D. Wilhoit	1029 Seventh Ave., Huntington, W. Va.
West Central Texas (North Texas Section)	E. N. Mitchell	H. L. Pearce, Jr.	1634 S. 7th St., Abilene, Texas
Western Virginia (Virginia Section)	G. L. Seay	J. B. Whitmore	P.O. Box 2021, Roanoke, Va.
Wilmington (Philadelphia Section)	Henry Evans	William J. Mearns	Hercules Powder Co., 900 Market St., Wilmington 99, Del.
Zanesville (Columbus Section)	R. P. Thompson	R. W. Hartsook	1637 Norwood Blvd., Zanesville, Ohio

Geographical District Executive Committees

District	Chairman (Vice-President, AIEE)	Secretary (District Secretary)	Chairman, District Committee on Student Activities
1 North Eastern	Victor Siegfried, American Steel & Wire Co., Worcester 7, Mass.	Frederick G. Webber, 9 Federal Court, Springfield 5, Mass.	E. R. McKee, University of Vermont, Burlington, Vt.
2 Middle Eastern	G. W. Bower, 127 Hopkins Ave., Haddonfield, N. J.	A. C. Muir, Berwind-White Coal Mining Co., 1138 Comm. Trust Bldg., Philadelphia 2, Pa.	Kenneth F. Sibila, University of Akron, Akron 4, Ohio
3 New York City	J. L. Callahan, R. C. A. Laboratories, Radio Corporation of America, 66 Broad St., New York 4, N. Y.	W. J. Barrett, New Jersey Bell Telephone Co., 540 Broad St., Newark 1, N. J.	F. A. Wahlers, Polytechnic Inst. of Brooklyn, 99 Livingston St., Brooklyn 2, N. Y.
4 Southern	J. H. Berry, Electric Dept., Virginia Electric and Power Co., Norfolk 1, Va.	T. H. Mawson, Commonwealth & Southern Corp., 600 N. 18th St., Birmingham 2, Ala.	L. V. McLean, Louisiana State University, Baton Rouge 3, La.
5 Great Lakes	E. W. Seeger, Cutler-Hammer, Inc., 315 North 12th St., Milwaukee 1, Wis.	N. C. Pearcy, Pioneer Service & Engineering Co., 231 South La Salle St., Chicago 4, Ill.	W. H. Gamble, South Dakota State College, Brookings, S. D.
6 North Central	I. M. Eliestad, Northwestern Bell Telephone Co., 823 Telephone Bldg., Omaha 2, Nebr.	M. L. Burgess, Westinghouse Electric Corp., 117 N. 13th St., Omaha 2, Nebr.	J. O. Kammerman, South Dakota School of Mines & Tech., Rapid City, S. D.
7 South West	G. N. Pingree, General Electric Co., 1801 North Lamar St., Dallas 2, Texas	Gibbs A. Dyer, Southwestern Bell Tel. Co., Dallas 1, Texas	F. W. Tatum, Southern Methodist University, Dallas 5, Texas
8 Pacific	D. I. Cone, Pacific Tel. & Tel. Co., 140 New Montgomery St., San Francisco 5, Calif.	B. D. Dexter, 245 Market St., San Francisco 6, Calif.	I. J. Sandorf, Univ. of Nevada, Reno, Nev.
9 North West	Richard McKay, Washington Water Power Co., West 825 Trent Ave., Spokane 6, Wash.	H. C. Glaze, Jr., General Electric Co., S. 162 Post St., Spokane 8, Wash.	Vinson L. Palmer, University of Washington, Seattle 5, Wash.
10 Canada	D. G. Geiger, Bell Telephone Co. of Canada Ltd., 76 Adelaide St. W., Toronto 1, Ont.	J. Taylor Fisher, 76 Adelaide St. West, Toronto 1, Ont.	W. B. Coulthard, University of British Columbia, Vancouver, B. C.

NOTE: Each District executive committee includes also the chairmen and secretaries of all Sections within the District, a member of the Sections Committee resident in the District, and the District vice-chairman of the AIEE membership committee.